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Preface

This 63d volume of the TRANSACTIONS of the American Institute of Electrical Engineers contains all approved technical-program papers and related discussions presented at national and District technical meetings during the calendar year, in accordance with the improved publication procedure adopted in August 1940. Pages 1–318 and 501–960 comprise the papers published in the Transactions Sections of the monthly issues of ELECTRICAL ENGINEERING, and pages 319–500 and 961–1504 comprise papers and discussions that appeared in advance in the June and December 1944 "Supplements to Electrical Engineering—Transactions Section," respectively.

The technical papers and discussions in this volume were presented at these meetings:

1. 1944 winter technical meeting, New York, N. Y., January 24–28, 1944.
2. North Eastern District technical meeting, Boston, Mass., April 19–20, 1944.
3. 1944 summer technical meeting, St. Louis, Mo., June 26–30, 1944.
4. Los Angeles technical meeting, Los Angeles, Calif., August 29–September 1, 1944.

In addition this volume contains:

1. A special report, "Recent Developments in Light Sources."
2. 1944 annual report of the AIEE board of directors.
3. A complete listing of AIEE officers and committees for 1944–45.

Full correlation of all material in this volume has been accomplished by means of the multientry index beginning on page 1539. Reference to any of the several subject entries for a technical paper will lead directly to the paper and to any published discussion on that paper.

Statements and opinions given in papers and discussions published in TRANSACTIONS are the expressions of contributors for which the American Institute of Electrical Engineers assumes no responsibility.

Errata

1. Page 552, Figure 7. The point for low-viscosity oil should be 37 degrees viscosity and 175 per cent ionization voltage increase.

2. Page 698, the author's name, following the title of the paper at the top of the page and in the footnote at the bottom of column 1, should be Wayne J. Morrill.

3. Page 882, column 3, third from last equation. RCF should read I_p/N.

4. Page 973, Figure 9. Symbols I_e should be used in place of I_A and α in place of ϕ.
5. Page 974. Figure 11. Symbol I_C should be used in place of I_A.

Study of Artificial Respiration on Anesthetized Men

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IN an earlier paper[1] the authors reported the results of a study of the methods of artificial respiration when conscious subjects were used. In that study three methods of artificial respiration were investigated: namely, the Schafer prone-pressure method, the modified prone-pressure method, in which the hands were rolled or snapped off the subject's back, and the pole-top method.[2] A total of 15 men was tested, and the volume of air moved by the three methods of artificial respiration was measured. All of these men were conscious, and the results indicated that all the methods were adequate, with the pole-top method in the lead. Although all of the subjects were intelligent and presumably capable of completely relaxed passivity, such results are always open to the criticism that the subject may have co-operated, perhaps unintentionally.

The investigation was, therefore, repeated, using anesthetized subjects in whom there is no possibility of co-operative aid. One of the barbiturates, sodium pentothal, was administered intravenously. This drug promptly effects unconsciousness and complete muscular relaxation. At the outset it depresses the respiration, which quickly assumes a comparatively steady state. This can be maintained safely for a considerable period of time by the judicious administration of repetitive doses of the drug. Subjects *A*, *B*, and *C* were studied and were carried through the procedure successfully.

Experimental Procedure

The subject's breathing was first measured while he was in a conscious state. A mask was placed firmly over his nose and mouth and connected to a spirometer (gas meter) which measured the volume of air breathed. A pneumograph was placed around the chest to ascertain the amount of chest expansion and contraction. A revolving smoked drum recorded the amount of air passing through the gas meter, the chest movement, and the time. The data thus secured served to determine the amount of air breathed in the unit of time necessary to complete the experiment.

The subject then placed himself in a prone position on a mat lying on the floor. After five minutes had been allowed for him to relax, a record of his natural breathing was taken. A trained operator then gave him artificial respiration, employing the standard Schafer method, and then after a suitable interval, the modified Schafer method. The conscious subject, with the mask still in place, climbed a pole that was mounted in a laboratory, and seated himself astride the safety strap of the operator. His position on the pole was similar to that of a lineman who had received a shock, except that his feet were only about one foot above the floor. The subject's natural breathing was recorded, and then the operator applied the pole-top method of artificial respiration.

The anesthetic was then administered to the subject while he was still on the pole and astride the operator's safety strap. The subject became unconscious quickly, and, after the elapse of a few minutes, his breathing reached a steady state. This was recorded. Then artificial respiration was administered by the operator, using the pole-top method.

When this was completed, the subject was lifted down from the pole, while still in an unconscious state, and placed in a prone position on the mat. The gas mask was not disturbed in this operation. The subject's natural breathing in this prone position and unconscious state was recorded. Then the operator applied artificial respiration by the standard and modified methods in turn. When the experiment had been completed, the administration of the anesthetic was stopped, and the gas mask and pneumograph were removed. The subject was made comfortable and allowed to rest until he was fully conscious. None of the subjects experienced ill effects from the procedure.

Pauses of several minutes were interspersed between each operation in order that the subject's respiration might become normal under the existing conditions before the next step of the experimental procedure. The results obtained in an experiment of this character are not strictly comparable to what might be expected in individuals in which all respiratory activity has ceased, but they probably are as close as laboratory conditions can provide.

The data taken are presented in Table I. It will be noted that the imposed respiratory rate during artificial respiration is less than in natural breathing. This is particularly true of the subjects when they are in an unconscious condition. The amount of air moved per respiration is, however, considerably greater. It is thus evident that all three methods of artificial respiration are more than adequate to supply the necessary ventilation of the lungs. In the last column of Table I the average results reported on the 15 conscious subjects in the earlier paper[1] are given for the purpose of comparison.

In Table II the data are expressed as

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Table I

Conditions	A 22 Years 139 Pounds	B 38 Years 160 Pounds	C 38 Years 167 Pounds	Average A, B, C	Average 15 Subjects
Conscious					
Natural breathing in prone position					
Respirations per minute	15	14	11	13	13
Cubic centimeters per respiration	375	625	770	590	545
Cubic centimeters per minute				7,670	7,090
Original Schafer artificial respiration*					
Respirations per minute	14	10	12	12	11
Cubic centimeters per respiration	424	1,000	1,111	845	868
Cubic centimet.rs per minute				10,150	9,540
Modified Schafer artificial respiration**					
Respirations per minute	13	11	10	11	11
Cubic centimeters per respiration	457	1,250	1,250	986	1,028
Cubic centimeters per minute				10,850	10,300
Natural breathing in trunk-vertical position					
Respirations per minute	14	16	10	13	15
Cubic centimeters per respiration	413	625	1,000	679	558
Cubic centimeters per minute				8,830	8,370
Pole top artificial respiration					
Respirations per minute	10	14	10	11	10
Cubic centimeters per respiration	875	1,111	1,111	1,032	1,363
Cubic centimeters per minute				11,340	13,630
Unconscious					
Natural breathing in prone position					
Respirations per minute	17	18	19	18	
Cubic centimeters per respiration	172	384	237	264	
Cubic centimeters per minute				4,752	
Original Schafer artificial respiration*					
Respiration per minute	11	11	12	11	
Cubic centimeters per respiration	430	714	625	590	
Cubic centimeters per minute				6,490	
Modified Schafer artificial respiration**					
Respirations per minute	9.5	10	12	10	
Cubic centimeters per respiration	571	910	768	746	
Cubic centimeters per minute				7,460	
Natural breathing in trunk-vertical position					
Respirations per minute	21	17	19	19	
Cubic centimeters per respiration	170	367	280	272	
Cubic centimeters per minute				5,168	
Pole-top artificial respiration					
Respirations per minute	11	12	13	12	
Cubic centimeters per respiration	714	912	770	799	
Cubic centimeters per minute				9,588	

* Hands held in position between pressure strokes.

** Hands allowed to slip off at the end of each pressure stroke.

Table II

	Percentage Increase Over Control Period—Air Moved Per Minute		Percentage Increase Over Control Period—Air Moved Per Respiration		Percentage Difference in Air Moved Per Minute by Artificial Respiration While Patient Is Anesthetized Compared With Normal Breathing While Patient Is Conscious
	Conscious	Anesthetized	Conscious	Anesthetized	
Original Schafer artificial respiration	32 (33) *	37	43 (59) *	123	15 per cent decrease
Modified Schafer artificial respiration	41 (60) *	57	67 (89) *	183	3 per cent decrease
Pole-top artificial method	29 (63) *	86	52 (144)*	194	9 per cent increase

* Percentages for 15 conscious subjects, see reference 1.

percentage increases or decreases of the amount of air moved per respiration and per minute over the corresponding periods of natural conscious breathing of the three subjects. It is evident that the air moved per respiration under artificial respiration is equal to, or greater than, the natural breathing of the conscious subjects. The number of respirations per minute is less for the artificial respiration than for the natural breathing of the conscious subjects. In the cases of both Schafer methods there is a decrease in the air moved per minute in the unconscious state as compared with the normal breathing. The pole-top method records a slight increase in the volume of air moved per minute. Had the tempo of artificial respiration been increased to 13 times per minute, all three methods would have given equal or greater volume of air breathed per minute than for natural conscious breathing.

The bracketed figures in Table II are taken from the earlier paper.[1] They show a higher percentage increase in the volume of air moved than was found for the three subjects reported here. This suggests that a psychological factor may be involved in working with conscious subjects. The results obtained with the unconscious subjects are, however, not subject to this factor.

Conclusions

These studies give evidence of the relative efficiency of the three methods of artificial respiration studied, and so far as we know they represent the first effort to evaluate the worth of artificial respiration[3] in subjects under general anesthesia.

The data presented indicate that an equal or greater amount of air is moved in the unconscious (anesthetized) than in the conscious subject with each respiratory act. The modified Schafer method (hands allowed to slip off at the end of every stroke) is superior to the standard Schafer method, and the pole-top method is more efficacious in the trunk vertical position than the Schafer methods in the prone position.

It is apparent that all three methods of artificial respiration studied are more than adequate to supply the requisite pulmonary ventilation.

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Aircraft Signal Systems

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Synopsis: Because the selection of aircraft warning devices has not, in most signal installations, provided the pilot with dependable warnings of dangerous conditions, this paper proposes that aircraft signal devices should be designed and installed in compliance with the pilot's requirements as expressed in three design principles.

The publication describes the weaknesses of current signal devices; it recommends signal light indicators that are bright enough to be seen under direct sunlight; and it illustrates and describes an automatic dimming control that operates as a function of natural light, independent of pilot actions.

FORGETFULNESS on the part of the pilot to extend retractable-type landing gears initiated the requirements for positive-warning signal systems on airplanes. Malfunctioning of important equipment and co-ordination of crew activities brought about the requirements for informative signals in aircraft.

In addition to these requirements, pilots demanded warning signals for such items as gears "not locked," flaps "not down," fuel pressure "low," fuel level "low," oil pressure "low," and cabin "pressurization" "off." Informative signals were demanded for oxygen pressure "low," warning of icing conditions, bombing signals, paratroop "jump" signals, passenger call signals, crew call signals, and so forth.

To provide these signals for the pilot's use, it is necessary that the signal indicators should be designed correctly and that airplanes should be equipped with the necessary mechanical or electric pickups, consisting of easily serviceable actuating devices or limit switches, pressure switches, temperature switches, and control relays. These electric-signal pickup devices and controls have been available for some time as serviceable and reliable parts and are beyond the scope of this paper.

The signal indicator is the only controversial item in the airplane signal system, and manufacturers, in attempting to provide suitable signal indicators, have resorted to mechanical flags, vibrators, bells, horns, position indicators, indicator lights, telltale panels, and similar devices to serve as warning signals and as informative signals. The selection of these devices for the signal indications on airplanes has been made, in too many cases, by compromise decisions which have been influenced by restrictions rather than by principles. The result of such selection of indicators is that many signal systems on airplanes are not serving the purpose intended.

The purpose of this paper, therefore, is to outline the design principles that aircraft signal indicators should satisfy, to discuss existing signal indicative devices and to what extent they comply with these principles, and to suggest signal-system methods for improving the pilot's "workshop" so as to relieve him of unnecessary duties and thereby lessen pilot fatigue.

Design Principles

The following principles usually are common to the design of all warning signal indicators and to the design of most of the informative signal indicators in aircraft signal systems.

SIGNALS MUST BE ABSOLUTE

The first principle to be satisfied in providing signal indications is that the signal device should, by independent action, bring about decisive, positive, and effective action. Thus, aircraft-warning signal devices should, in themselves, provide indications which incite the pilot's reflexes in such a manner that he will change quickly and unquestionably to the obvious alternate course of action. Informative signal devices complying with this principle should show, in addition, directive indications. This directive-type indication is necessary to avoid creating any degree of confusion in the mind of the pilot or crew member, as the course of action may be perplexing for the moment.

SIGNALS DISTINCT

The second basic rule to be followed in designing a signal indicator is to construct the indicative device so that it will emit signals which are distinct, abrupt, attractive, inescapable, and in definite contrast to everything in the proximity of the signal during any flight condition.

SIGNALS NOT A HAZARD

The third principle to be observed in designing signal indications is to be sure that the signal does not constitute a hazard and thereby handicap the pilot in performing his operations. Violation of this principle is exemplified in installations where the brilliance of light signals is not modulated as a function of the natural light.

These principles are not new, and they are seldom ignored by the commercial control engineer. Most aircraft engineers would like to follow these laws but cannot because of the inertia of the aircraft industry against cost of the correct device in terms of weight, special design studies, and manufacturing or procurement complications. It is because of this cumbersome inertia and a desire for improvement of aircraft signal systems that the comparative description of signal devices is discussed in detail in the next section of this paper.

Existing Systems

VISUAL MECHANICAL SIGNALS

Flags and mechanical indicators are the oldest type of warning and informative signal devices. They have been used in the airplane industry on small airplanes for such items as landing gear, flap, and tab position indicators. Examples of this type include push-rod and vane indicators on landing-gear mechanisms, spring- or cable-actuated scale and pointer combinations, solenoid-operated flags, and solenoid-operated targets that become visible by natural light or black light.

These devices have been selected by airplane manufacturers, because they are simple to design, install, and maintain, and because of the small number of parts that need to be replaced.

Fundamentally, all of these visual-type indicators depend on their warning by reflected light. The dependence on auxiliary light sources, and the fact that the signals are not abrupt or distinct enough to attract consistently the pilot's attention cause these signals to be considered undesirable warning signals.

In general, it may be stated that flags, mechanical indicators, and solenoid-actuated indicators obviously comply with the third principle, but do not satisfy completely the first and second principles discussed in the preceding section. These devices do not satisfy the first principle,

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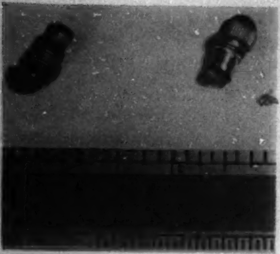

Figure 1. Lens-end signal lamps. General Electric Company T 1$^{1}/_{4}$, 3 volts, 3 amperes, BM base

because they are not self-sufficient in themselves, for the reasons that they require auxiliary illumination, are not always positive, and, therefore, do not bring about immediate action. This type of signal does not comply with the second principle, because its indications are not abrupt enough, are not inescapable from the pilot's vision, and are not in sharp contrast to all other images meeting the eye.

VIBRATORS

Foot-pedal vibrator-type signals partly comply with all three design principles, but in most installations signals of this type do not fulfill the requirements of the first and second principles. Many pilots have expressed difficulty in interpreting the meaning of the signal, while others complained of the lack of contrast of the vibration signal with the normal vibration of the airplane.

AUDIBLE SIGNALS

On practically all large airplanes designers have installed audible-type signals such as high-frequency vibrators, bells, and horns as warning and informative signal devices. Although these audible signals, when properly installed, comply with all three of the design principles, the characteristics of this type of signal limits the installation to one or two signals (the distance warning signals can be installed from the pilot's ear) and to the objections raised by air-line operators if warning or informative call signals can be heard by the passengers.

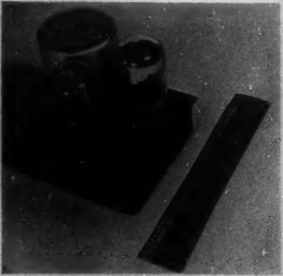

Figure 2. Photoelectronic dimming control

The advantages claimed for this type signal include:

1. The convenience of mounting in waste spaces.
2. The fact that no modulation of the signal volume is required during flight operations.
3. The fact that the need of replacements is rare.
4. Convenience of electric wiring for remote actuation of the audible indicator.

An example of audible warning or informative-signal indicator is the vibrator-type horn built to conform to Army-Navy specification *AN-S*-8.

Figure 3. Telltale device as installed in Curtiss-Wright *AT-9* airplanes

This telltale is a visual warning device mounted on the instrument panel and contains 18 warning and informative signals as shown. Constant checking and rechecking of the instruments by the pilots has been eliminated, as this device is electrically connected to necessary instruments, controls, and component operating parts of the airplane, in such manner as to report immediately and constantly on their operation. These operations are shown by individual lighted signals on the telltale panel which are operated by limit switches, pressure switches, and so forth. The signals, as arranged, function as a "negative-warning" signal device, as a perfectly dark panel indicates correct operation of the items

INSTRUMENT-TYPE SIGNAL INDICATIONS

Position indicators in some cases have been used to provide warning or danger signals as well as informative signals. Many aircraft manufacturers have installed indicators of this type for the purpose of combining warning or danger signals with informative position indications in a centralized location.

These combination position and warning-signal indicators are available with two types of warning signals. In one type the warning signals are manifest by mechanical flags which disappear when the landing-gear units are in the locked position. The other type indicator utilizes signal lamps as warning signals.

This combination instrument, known as *GE* 8*DJ*4*Y*6, when equipped with the signal lights, is an excellent centralized signal and position device in that it satisfies all three of the design principles, provided that the brilliance of the light matches the natural light. As will be discussed under light signals, the brilliancy of the signal lights should be controlled by an automatic modulating or automatic step-type control to maintain proper brilliancy of the signal lamp to satisfy night and day flight conditions.

LIGHT SIGNALS

Colored lights have been used very effectively as warning and informative signals on all types of airplanes. The installation of these lights as warning signals has been accomplished in the conventional industrial manner, that is, a colored lens shielding a clear electric lamp.

The greatest disadvantage of light signals in any signal system is the necessity for controlling the brilliancy of the signal light as a function of the natural light. It is this disadvantage that causes the criticism of light signals. This criticism is, of course, warranted, as light signals that are too dim to be distinct in bright sunlight do not comply with the second design principle, whereas light signals that are too bright at night constitute a hazard to the pilot.

In view of this brightness factor, most airplane manufacturers have provided manually operated mechanical or electrical dimming devices to adjust the brilliance of the light signal to correspond to the natural light in the cockpit or control room. These manual devices, however, do not solve the light-intensity control problem for the reason that the pilot or crew member is not reminded to turn up the intensity of the signal light to match the sunlight conditions. Thus, it often happens during daylight operation that the warning light is energized, but no signal warning is noticed by the pilot for the reason that the light had been dimmed previously for night intensity. Even with semiautomatic schemes this condition still exists. For example, in signal-light installations where dimming is accomplished by the introduction of resistors by relay in the signal-light circuit by turning the navigation lights "on," the failure to increase the signal intensity is not prevented. Only the possibilities are reduced.

BRIGHT-BEAM LIGHT AND PHOTOELECTRONIC DIMMING DEVICE

This year a signal-light system has been perfected that will satisfy all three design principles. Figure 1 shows the signal light itself, and Figure 2 illustrates the photoelectronic dimming device which controls the brilliance of any number of signal lights as a function of the natural light without any attention on the part of the pilot or crew member.

NEW SIGNAL LAMP

The lamp shown in Figure 1, although similar to the General Electric Mazda 319 or 320 28-volt bombing signal lamp, is identified as a General Electric *T* $1^1/_4$, three-volt 0.30-ampere lens-end *BM*-type-base signal lamp.* This lamp was developed to produce a high-candle-power light beam which would serve as a distinct and inescapable light signal in direct sunlight. As of this writing, this lamp is the only unshielded signal lamp, suitable for aircraft uses, with which off and on can be distinguished readily under the direct rays of the sun.

This lamp was designed especially for low-voltage operation to insure long life (at least 250 hours) and to use a heavier and less-destructible filament. The lens in the lamp end is similar to the lens used in a "penlite"-type flash-light lamp. The lamps have been supplied in red, amber, blue, clear, and green.

PHOTOELECTRONIC DIMMING DEVICE

The photoelectronic dimming-control device shown in Figure 2 is referred to as General Electric 7505-*L*-108. This automatic-control unit can be obtained to actuate automatically a relay, to open or close resistor circuits, which dims or brightens any number of signal lamps in accordance with the requirements for twilight or daylight.

As the photoelectronic tube is sensitive to any degree of light changes from one-foot candle and upward, the selection of photoelectronic units for any particular airplane can be obtained for dimming control in as many steps as desired.

The photoelectronic device shown in Figure 2 consists of a photoelectric tube, amplifier tube, sensitive relay, resistor, and mounting. The unit, as pictured, weighs less than a pound and operates from any 24-volt d-c supply.

ELECTRIC CIGARETTE-LIGHTER-TYPE DIMMING CONTROL

Another automatic device has been designed to control the dimming of signal lights, but the weight is about the same, and it does not offer complete control. This proposed device is a thermal time-delay switch which instantly dims the signal light for a five-minute period only, then snaps to the original bright position in much the same manner as an electric cigarette lighter. This device does not permit the signal-system installation to satisfy completely the third design principle, as it repeatedly constitutes a momentary hazard during night-flight operations.

TELLTALE

The telltale device shown in Figure 3, manufactured by the American Automatic Company *GH*-70,350, is a centralized signal indicator which incorporates all of the control-room signals into one compact panel. Installation, as made on many hundreds of airplanes, provides for 18 light-signal indications with the corresponding illuminated-directive nomenclature all arranged on a panel 3 by $3^1/_2$ inches.

This device, in addition to showing warning and informative signals, includes directive signals to indicate to the pilot incorrect setting of important controls such as propeller, mixture, manifold pressure, carburetor heat, and trim tabs. Actuation of these directive signals in the telltale panel is accomplished by automatic switching that is a combined function of the control setting and the flight regime.

Considered as a whole, this device complies with all three signal-indication design principles, except those conditions under which the second principle is violated. Like the manual-control shutter-dimmed signal lamps previously mentioned, the telltale unit is susceptible to daytime operating conditions where the dimming shield could be partially closed for night operations, and the pilot would overlook the telltale signals. As was discussed under "Light Signals," an automatic dimming device would correct this difficulty, and the telltale signal indicator would then satisfy all three design principles.

Concluding Recommendations

From the discussion given in this paper, it is recommended that all warning and informative signals should be light signals which are bright enough to be seen under direct sunlight. As part of this recommendation, it is suggested that the light intensity of the warning and informative signals should be controlled automatically as a function of day or night operation. For special requirements the light intensity should be modulated automatically or adjusted in steps to correspond with the natural light. Referring to the three design principles mentioned at the beginning of this paper, it is believed that a signal indicator complying with these suggestions will provide light signals which are absolute, always can be detected, and will not constitute a hazard to the pilot.

It is further suggested that to reduce pilot fatigue and to eliminate unnecessary searching of instrument indications more signals should be added to the airplane signal system. By the same reasoning it is recommended that these additional signals should be grouped into a centralized location in a manner similar to that utilized by the telltale panel shown in Figure 3.

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* *BM*-type base refers to the special lamp base designed to match the Bardwell-McAllister lamp socket.

Cultural Training of the Engineer

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IT is generally recognized that a professional man must have a certain amount of cultural training, in addition to the technical, so as to be better balanced, more useful to the society, and more successful; and engineering curricula generally make provision for it. However, it appears questionable whether the cultural program is as well suited to its objectives as the technical programs are to theirs. As a parent, the author had occasion to look into this matter during the past several years and was led to the conclusion that the quality of the conception and execution of the cultural training of the engineer is definitely inferior to that of his technical training.

Although the author would be pleased if this paper should stimulate a general discussion and careful review of the situation by competent authorities, leading to its amelioration, yet his immediate purpose is more modest—to share some of his thoughts on this matter with the parents of prospective technical students.*

A Basis for Planning

Considering the great variety of cultural subjects and the wide divergence of tastes, it may be a question whether a generally acceptable cultural program can be planned. Think of the cultural value and fascination of the fine arts—music, painting, sculpture, architecture, dramatics, dancing, and so forth. Of the classics—poetry, fiction, drama, essay, mythology, and so forth. Of philosophy—logic, metaphysics, and so forth. Of the social subjects—sociology, anthropology, even archeology, politics, economics, and so forth. And history—political, social, revolutionary, and so forth. And—but why enumerate more?

What shall we recommend to our sons specializing in science or technology? Is there a basis for choice other than individual taste?

I think there is such a basis, and it is to be found in certain common and urgent needs of the average student. The shortage of available time for this purpose makes it imperative to pass by most of the fascinating subjects mentioned, to assure that certain "must" subjects are properly taken care of first.

Student's Needs

I think we can get general agreement on at least the following three cultural needs of the average student as fundamental:

1. Knowledge of and skill in studying efficiently.
2. Knowledge of and skill in dealing with himself and others successfully.
3. Ability to express himself well in speech and writing.

Let us examine each item in its turn.

1. How to study is perhaps the most pressing problem of the freshman. Little or no instruction is given in this matter by the faculty; in its place, frequent quizzes are given to "put the screws on" the freshman. He must discover for himself how to study or be eliminated. This should have been learned at the high school, but it was not. The fellow was then still a "darling boy," and we regretted that he had to do homework at all, instead of being out in the fresh air or in bed, or having fun with other young people—boys and girls. Unprepared for serious study, the freshmen fret and swear, smoke and scribble and spit, and, finally, about half of them evolve some temporary *modus vivendi* whereby they survive for four years to receive their diplomas. After that, books and studies may go overboard. Four years of those things were too much already!

2. How to deal with himself and others is another great problem of the student, but he is not generally aware of it, and no particular attention is paid to it by the faculty.

Years ago, at a gathering of young engineers, a big engineering executive was asked for the key to success. "Boys," he said, "Pay close attention to your mathematics;" but, later, when the same question was put to him again, now by a friend of his and in the privacy of his home library, the answer was: "George, the key to success in engineering is 40 per cent technology, 60 per cent diplomacy."

Whether it is called diplomacy, or social psychology, or simply human relations, a certain amount of proficiency in it is essential in the successful practice of engineering. More of it is required, of course, in managerial positions than in the ranks, but that the latter group frequently do not possess even the modicum that they need is indicated by the published experience of one large organization to the effect that in a period of depression the selection of technical men for layoff was more often for lack of co-operativeness than for lack of technical ability.[1]

It is probably safe to predict that the student who is brilliant in technology but just passable in human relations will in time work for the one who was just passable in technology but brilliant in human relations.

3. As for self-expression, technical schools pay only a limited attention to speech but are generally interested in writing, and in this the freshman is traditionally deficient. This deficiency is supposed to be due to lack of knowledge of the English language, and so the freshman is required to take English. But the principles of good writing are the same in any language, so good writing cannot be a matter of knowing the English language. Good writing can be done (frequently is done by the masters) in various dialects and slang; it can be done in basic English, even in pigeon English! Think of the gems in negro dialect! Knowledge of good English usage is of course highly desirable, but too much is expected of it when it is assumed that knowledge of English is the main thing in writing well.

Just because writing is important, speaking should not be treated casually. Speaking ability is a greater asset for an executive than writing ability. After all, should the necessity arise, it is possible to hire someone to write for you but not to speak for you. Furthermore, a good speaker can always produce a good manuscript by dictating as if he were giving a talk; but let a person talk as if he were writing a technical paper—what a disappointment that would be! Dr. Heiser has told us the secret of how he wrote his best seller, "An American Doctor's Odyssey." He realized that if he sat down to write his experiences, the product would probably be "a dry Rockefeller Foundation report." So he changed his plan. He knew that through years of practice he could talk fairly interestingly, so he hired an alert newspaperman to interview him, and a stenographer to record the interview. A best seller was

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* The views expressed here are those of the author himself and do not necessarily represent those of the company with which he is associated.

thus produced in a short time and without the usual drudgery of book writing.

I believe that for the average professional man the ability to speak before a group clearly, convincingly, and gracefully is a greater asset than ability to write well.

Urgency of the Problem

These three needs of the student cannot be postponed. He must acquire in his freshman year not only an efficient study technique, but also a variety of other habits essential to a successful personality. The college term, particularly the freshman year, is the terminal period of transition from the plastic to the relatively nonplastic phase of life and may be looked upon as the student's last opportunity to shape his personality into a desirable pattern. Soon he will be "set"; he will become a mere "walking bundle of habits," to quote William James, the psychologist. A complex network of intellectual, emotional, and motor habits will then constitute his entire personality and will determine even the details of his professional style and judgment. If this complex of habits is developed haphazardly and unawares, what probability would there be of anything but a mediocre personality developing, unless an exceptional heredity or a great inspiration or an exceptional set of circumstances should exist?

Credit units in classics and metaphysical speculation may be helpful but would not assure the kind of culture that a parent is likely to contemplate for his son. Furthermore, culture has not been acquired until it has become second nature through habit. The college years therefore should be utilized eagerly as the student's last opportunity to form these habits by practice with reasonable ease and under competent guidance.

Meeting the Needs

The basic cultural course generally required at the present time is English, but it is the opinion of this writer that if the foregoing needs of the student are true, they can be met best by practical psychology. It is not advocated that English be dropped but rather that it be approached as a problem in psychology rather than one in language.

Of course, a course in psychology is not necessarily cultural. It will not be so unless it is competently planned and conducted for this end. Let us note some of the features of such a course.

(*a*). The most important topic in practical psychology for our purpose is that of habit. The classic Chapter III on habit in James's two-volume psychology (or its equivalent) should be used as the text for half a dozen lessons, coupled with a judiciously chosen set of exercises in habit formation. The student should realize that there is infinitely more to habit than nail-biting or smoking; that it is the one great principle of personal efficiency, because it is the principle of mechanization—automatization—of human behavior; and that not the breaking of so-called bad habits but the building up of his idealized social and professional personality through conscious, systematic, and habitual formation of habits is the major interest of habit psychology. As the student is made habit-conscious and as he develops a few judiciously chosen pleasant and profitable habits as laboratory exercises and gets the "feel" of it, he will be incidentally forming the most important habit—the master habit—namely, the habit of soundly planning and promptly mechanizing as much of his routine life as possible so that his mind may be relieved of routine drudgery and be set free to engage itself in more pleasant tasks and greater achievements. With proper work, study, play, and social habits formed (or at least well started) in the freshman year, the student is prepared for the most efficient utilization of his time and abilities for the development of his professional skills and personality during the following three years.

Habit psychology is a basic principle of any training, for training is primarily the conversion of knowledge into habit patterns by practice. If one thinks that the development of skill, rather than that of habit patterns, is the primary feature of training, he may do well to consult a good reference on habit psychology, in which he will discover that habit formation is the foundation of development of skills; for, after all, the major instrumentality in the development of skills is practice, and practice is inseparable from habit formation.

Many psychologists, engrossed in their specialties, do not seem to be actively aware of these things. The eminent psychologist, Dr. Link, has confessed as much—that he was not alive to these things until he observed the needs of his adolescent children.[2,3]

(*b*). The next most important topic in practical psychology is human motivation. Properly taught, this should enable the student to see quickly the other fellow's point of view which generally amounts to his motivation. Much of our nontechnical reasoning, especially in arguments, is nothing but rationalization, defense, and wishful thinking: in other words, biased or emotional thinking. That is why arguers never get anywhere with proofs. That is why an appropriate incentive convinces people so much better than logical evidence; and, as Hitler has demonstrated it, if the incentive is there, people will easily believe the biggest lies. "The will to believe . . ."

(*c*). The thought process and principles of clear thinking are naturally another important topic in psychology to be made clear to the freshman. Advertising psychology, political publicity, propaganda analysis, and scientific thinking, may be treated here in condensed form with great benefit and interest to the student.

(*d*). Then the eyes of the student must be opened to the power of attention and its significance towards his academic success. He should be enabled to see that the marvels of hypnotism are merely the product of an undistracted and concentrated attention; and that, if he wishes to be able to match the apparently superior power, intelligence, and memory of a hypnotized person, without the hocus-pocus of hypnotism, he can do so by cultivating his power of concentration. Would he then have any difficulty in studying at college or concentrating on creative work later?

(*e*). Practical psychology naturally forms the foundation of successful public speaking also. Many teachers of public speaking preoccupy the class with voice, posture, and other details to the exclusion of a psychological analysis of why a given speech is good or bad. "Not what to say but how to say it" is the watchword of some of them, the "how" being interpreted rather narrowly. But "what to say" is incomparably more important than "how to say it." If the presentations of papers at the AIEE meetings are frequently unsatisfactory, the basic reason, I think, is a lack of understanding of the psychology of the audience. The speaker probably is talking to a few specialists like himself sitting near the front to hear him well. That is why the rest don't hear him well; and when they do hear him, they do not understand what he is saying. I expect that if the psychology of the speaker is corrected, his voice and other important faults will automatically correct themselves.

Out of the mass of thoughts that are always available within the scope of the subject and the knowledge of the speaker, the selection of those which are best adapted to the background of the audience, the arrangement of those thoughts in a logical sequence, the phrasing of them in graceful fashion, and their presentation in an audible pleasing manner, is the common sense of good speaking as the audience itself would define it; and the audience, as the customer, is right.

It is well enough to have public speaking taught to technical students by professional public-speaking teachers, but in some cases it may be necessary to supplement such instruction by having one or more technical men gifted in public speaking and able to make psychological analysis of a given speech sit in at the classes as audience and critics.

(*f*). The basis of good technical writing also has to be practical—common sense—psychology. This requires the selection of essential information, its arrangement in logical order, and its expression in a manner that will be clear and interesting to the intended readers. How little the study of the English language has to do with this! The knowledge of English—vocabulary and grammar—of the average freshman is fully adequate for his needs. The training he needs is in organizing his thoughts and expressing them in the words he knows. Of course, the study of the masters of English style is helpful and to be recommended within the very limited time available for that particular purpose, but we must not also forget the desirability of developing the student's own individual style. Too much emphasis on masters can destroy the student's individual style.

The merits of the student's English composition or technical report should be analyzed and appraised not from the standpoint of language but from the standpoint of the psychological principles of holding the reader's attention, imparting information to him and influencing his reaction in a desired direction. A course in English composition in which the instructor does no more than editorial corrections, I should consider a waste of student's time.

(*g*). The success of a course in English literature as a cultural medium depends on how it is selected and how approached. The point of view of a professional "litterateur" can have little cultural value for the engineer. For instance, I pick up a book by an eminent literary critic, entitled something like "The Stories I Have Enjoyed." I read a selection and am disgusted—no big idea, no real human interest in it. Then I find out that the critic selected it because the technique of that kind of a story is considered very difficult, and the author of this story "got away with it." Over and over again I have observed literary critics (and musical critics) discuss at length considerations that can be of very little interest to those with only a general cultural interest in the matter.

What makes great literature great—the kind that lives through the centuries—is not its linguistic skill, but the profound insight into human nature that it exhibits; for instance, Shakespeare. But even the cultural value of a course in Shakespeare can be spoiled for the engineer by approaching it from the standpoint of literary technique.

In order to serve best the interests of the engineer, the teacher of literature must bear in mind constantly that what is needed is primarily a literature-appreciation course, carrying with it the following corollaries:

1. The teacher will have to work harder than the students. Especially in the composition work, he should be able and willing to rewrite the student's material and explain to him why one was bad and the other one is good.

2. Selections for reading must be chosen carefully and abridged to the limit to save the student's time. It will be better to err on the side of making it a "snap" course, rather than a difficult course which will antagonize the student and make him hate literature ever after. I should consider it a highly successful course if in literature the teacher accomplished no more than infusing the student with such an appreciation of good literature that the student will continue reading it in later years.

3. Engineering students should not be combined with those majoring in literature and made to follow the latter's volume of work and point of view.

Broader Obligations

The engineer is frequently criticized for not playing a leading part in the social, civic, and political problems of his community. To remedy it may require more than one measure, but it appears to this author that the first step in effective preparation for such leadership is for the engineer to understand the psychology of himself and his fellowmen. That done, a course in sociology, or government, or economics, and so forth, may be added if time permits. In any case, if the graduate is well grounded in the general principles of human behavior and relationships and has had some systematic practice in college, he can obtain later, by private study, any additional knowledge needed for the civic activity into which he may wish to enter effectively.

Life has many surprises, and we may at any time be thrown into unexpected problem-situations. In such cases, we may count on the engineer to solve the technical problems involved, even though they may be far outside of his specialty, and we may fear his failing on account of mismanagement of the human problems involved, unless he has had special training or was born with an instinct for the human equation. War is such a situation.

At the AIEE Northeastern District meeting in Pittsfield (April 1943), Colonel Beukema told us that the reports received from the officers in the field indicate that the soldiers with cultural training make better leaders, and therefore the Army specialized training program is making some provision for such training.[4] I understand that the Navy program is even more generous in this respect. While I do not know whether the Army or the Navy has made any appraisal of the value of different cultural courses for its purposes, the type of basic cultural training here advocated would seem to be superior to classics, philosophy, and so forth, for the fighting man.

Problem of Time and Place

The minimum cultural training advocated here can certainly be had in the time now allotted for the humanities. It appears to this writer, however, that those who are particular about the nature and quality of such training are likely to ask for more of it and for some additional subjects, thereby prolonging the curriculum by another half year or full year. In normal times and the long run, I think the student is likely to be well repaid for the extra time. A five-year curriculum for the engineer is not unreasonable compared with the seven years many chemists are putting in now (to secure a degree of doctor of philosophy), the eight years that physicians generally put in, and the five to seven years that the requirements of some of the graduate schools of engineering entail.

As to the place, as things stand now, some students may find it necessary to consider the advisability of dividing their time between two institutions, one for the cultural courses, the other for engineering, especially if an extra year is to be devoted to the purpose. This naturally raises questions of loyalty and the difficulty of achieving advanced positions in the social and extracurricular activities in either institution, which in their turn work contrary to the major goal. I think certain arrangements have less objectionable features than others. The most proper solution of the problem is that the cultural curricula of the engineering schools be made as efficient as their engineering curricula.

Design of Starting Windings for Split-Phase Motors

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ALTHOUGH split-phase induction motors are built annually in millions of units, involving thousands of designs, little can be found in the technical literature concerning the design processes.

The usual procedure is to fix the main stator winding and the squirrel-cage rotor from specifications covering the maximum torque and the general running performance. The procedure for the direct determination of the stator winding to yield a required maximum torque is already available.[1]

The problem presented in design, after the determination of the main winding, involves the selection of starting winding turns and wire size. It is generally necessary that the completed design meet specified values of starting torque without exceeding limits of starting amperes. Such limits may be fixed by various standards or by customer's requirements.

It is very likely that some element of trial and error exists in the methods most commonly employed for starting winding design. An obvious approach is to assume a starting winding, calculate the circuit constants, and from these obtain the calculated values of starting torque and current. If the values are not suitable, a readjustment of the winding is necessary. Such readjustment may result in satisfactory values for one critical factor but not another. Continued trial and error may result in a final, satisfactory design. In such a process, the designer has three variables which can be controlled, namely winding distribution, number of turns, and wire size.

On the completion of a satisfactory design, the designer may still be troubled by the suspicion that, had some other combination of factors been used, still better results might have followed.

It is the purpose of this paper to present a method of design which will yield directly the starting winding turns required:

(*a*). For a given starting torque.

(*b*). For a given starting current.

If either is used as a criterion for design, it is always necessary to check the other characteristic. This can be done by referring to a few simple curve sheets, requiring no complicated calculation. A number of other relationships are developed, enabling the designer to apply proportion more intelligently in modifying existing designs.

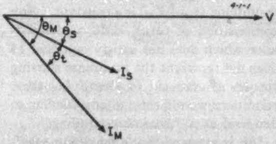

Figure 1. Vector relationships at instant of starting

The writers have investigated the possibilities of making up a series of curve sheets covering all reasonable values of split-phase winding parameters to enable torque and current to be read more or less directly, with little calculation. Because of the number of quantities involved, such an approach seemed impractical

Method of Approach

The items determining starting performance are chiefly:

(*a*). Rotor resistance and leakage reactance; main winding resistance, and leakage reactance.

(*b*). Starting winding resistance and leakage reactance.

The rotor quantities, referred to the stator main winding, differ in value from those same quantities, when referred to the starting winding, by the square of the transformation ratio. The starting winding reactance differs from the main by the same ratio squared. Hence, all starting winding parameters can be obtained in terms of the main winding, excepting the starting winding resistance. The method employed in this paper is to obtain the starting winding resistance in main winding terms. When that is done, *all* starting winding parameters can be expressed as a function of the main winding, and it in turn has been fixed by previous considerations. The resulting calculations are greatly simplified.

Expressions for Starting Torque

Several formulas are available for starting torque. A simple form is:

$$T=\frac{225.4}{\text{Syn. rpm}} I_M I_S \frac{r_2}{a} \sin \theta_t \text{ (ounce-feet)} \quad (1)$$

All terms are defined in the list of symbols.

This formula is conveniently expanded thus:

$$I_M=\frac{V}{Z_M} \quad (2)$$

$$I_s=\frac{V}{Z_s} \quad (3)$$

$$\theta_t=\theta_M-\theta_s \quad (4)$$

By the trigonometric expression for the difference of two angles:

$$\sin \theta_t=\frac{X_M R_s-R_M X_s}{Z_M Z_s} \quad (5)$$

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Since the acceptance of this paper by the Institute, the author's attention has been called to two articles in *The Journal of Engineering Education*, one describing the Army Specialized Training Program,[5] the other that of the Navy.[6] The first contains a splendid outline of the objectives in the English course. The Navy program is particularly interesting in that it prescribes for the electrical engineers a course in English in the first year and in psychology in the second and fourth years. The details of these courses are not outlined.

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1. CHARACTERISTICS OF A GROUP OF ENGINEERS, Thomas Spooner. AIEE TRANSACTIONS, volume 53, 1934, pages 1571–6; discussion, volume 54, 1935, pages 757–60.

2. THE RETURN TO RELIGION, Henry C. Link. Chapter VI, "Children Are Made," pages 93–7.

3. PERSONALITY CAN BE ACQUIRED, Henry C. Link. *Readers Digest*, December 1936.

4. TRAINING TECHNICAL LEADERS FOR WAR—THE ARMY PROGRAM, Herman Beukema. ELECTRICAL ENGINEERING, volume 62, March 1943, pages 89–93.

5. *The Journal of Engineering Education*, April 1943, page 558.

6. *The Journal of Engineering Education*, April 1943, page 630.

Using these items, we then have:

$$T=\frac{225.4V^2}{\text{Syn. rpm}}\cdot\frac{r_2}{a}\cdot\frac{X_MR_s-R_MX_s}{Z_M^2Z_s^2}\qquad\text{(ounce-feet)}\quad(6)$$

As shown here, the equation assumes that the equivalent circuits of the main and start windings are each simple series circuits, containing no magnetizing current component. If a magnetizing branch is assumed to be connected across the circuit midway between the stator and rotor constants, then the so-called theoretically exact circuit results, with greater accuracy of calculation. To accomplish this greater accuracy, the flux factor K_r will be used as shown. We then have

$$R_M=r_{1M}+K_rr_2=r_{1M}+r_0\qquad(7)$$

$$X_M=x_{1M}+\sqrt{K_r}x_2\qquad(8)$$

It will be assumed, therefore, throughout this paper that the leakage reactance for the main winding of the stator and the rotor is expressed as shown in equation 8, and that r_0 is used in place of r_2. This compensates for the effect of the magnetizing current component. We can now write the following equation for the terms used:

Total impedance in main winding items:

$$Z_M=\sqrt{R_M^2+X_M^2}\qquad(9)$$

Total resistance in starting winding terms:

$$R_s=r_{1s}+(r_0/a^2)\qquad(10)$$

Total leakage reactance in starting winding terms:

$$X_s=(X_M/a^2)\qquad(11)$$

Total impedance in starting winding terms:

$$Z_s=\sqrt{R_s^2+X_s^2}\qquad(12)$$

The ratio of transformation:

$$a=\frac{C_MK_{WM}}{C_SK_{WS}}=\sqrt{\frac{X_M}{X_s}}\qquad(13)$$

Some comment should be made concerning this ratio. Theoretically, the correct value would involve the turn ratio. It is customary to correct the leakage reactances as calculated by applying a constant which is a function of the winding factor, K_W. One such correction curve in common use[3] is nearly parabolic in shape. On that portion of the curve where it follows such a law, the ratio of transformation obtained from the effective turns is exactly the same as the ratio obtained from the leakage reactances. For high values of K_W a slight discrepancy results. This will be neglected here and the relationships of equation 13 assumed as uniformly accurate.

If equation 6, with the expanded constants of equations 7 to 12 substituted, is differentiated with respect to starting winding resistance (treating a as a constant), the condition required for maximum starting torque can be obtained. This condition is:

$$r_{1s}=\frac{1}{a^2}(r_{1M}+Z_M)\qquad(14)$$

Since a may have *any* value, the conditions in equation 14 can be satisfied by an infinite number of starting windings, and, so long as a^2 times r_{1S} equals $(r_{1M}+Z_M)$, each will yield a different maximum starting torque. It is important to note that there is no single value of maximum starting torque. The significance of the solution for maximum rests in this fact: with the main winding fixed by running conditions $(r_{1M}+Z_M)$ is a constant. Any combination of turns, ratio, and resistance which does not satisfy equation 14 does not represent the maximum starting torque. As an aid in identifying these conditions we will refer to any winding so designed as a "maximum winding."

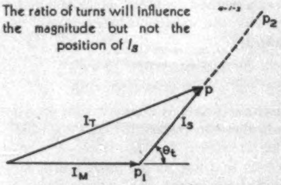
The ratio of turns will influence the magnitude but not the position of I_S
P_2
P
I_T
I_S
θ_t
I_M
P_1

Figure 2. The angle θ_t is fixed for all maximum windings which can be supplied

If it is assumed that a maximum winding is always to be supplied in the derivations which follow, we can write the following equations:

$$R_S=r_{1s}+\frac{r_0}{a^2}=\frac{r_{1M}+Z_M}{a^2}+\frac{r_0}{a^2}\qquad(15)$$

$$R_S=\frac{1}{a^2}(R_M+Z_M)\qquad(16)$$

$$Z_S=\frac{1}{a^2}\sqrt{2Z_M(Z_M+R_M)}\qquad(17)$$

These values can now be substituted in the starting torque equation, yielding the following:

$$T=\frac{112.7r_0a}{\text{Syn.rpm}}\cdot\frac{V^2}{Z_M^2}\cdot\frac{X_M}{Z_M+R_M}\qquad\text{(ounce-feet)}\ (18)$$

This is the maximum starting torque for a given ratio a.

The preceding formulas show two significant facts.

As the starting winding is changed, keeping the product $r_{1S}a^2$ at the correct constant value, the starting torque will increase directly as the ratio of transformation a. The current in the starting winding will vary directly as a^2. This follows from equation 17.

Slot Space Required

With the various sizes of wire and number of turns available for maximum windings, the question naturally arises as to the relative slot area required for the various combinations. This will be investigated.

In general:

$$r_{1S}=0.87\frac{l_cC_S}{A}\qquad(19)$$

$$a=\frac{C_MK_{WM}}{C_SK_{WS}}\ \text{(from equation 13)}$$

Therefore

$$r_{1S}a^2=\frac{0.87l_c}{A}\cdot\frac{K_{WM}^2C_M^2}{C_SK_{WS}^2}=(r_{1M}+Z_M)\qquad(20)$$

or

$$AC_S=\left(\frac{0.87K_{WM}^2C_M^2}{(r_{1M}+Z_M)}\right)\left[\frac{l_c}{K_{WS}^2}\right]\qquad(21)$$

The terms in parentheses all apply to the main winding and are not subject to change. The terms in brackets are fixed by the starting winding distribution but are independent of the number of starting winding turns. Hence, the right-hand member of this equation is a constant for a given starting winding distribution. The product AC_S represents the circular mils per conductor times the number of starting winding conductors or the total useful slot area devoted to starting winding. Hence, regardless of the various combinations of turns and wire sizes, if they result in maximum windings, the total slot areas required for the starting windings are identical.

Or, if the total slot area for starting windings is fixed by heating considerations or otherwise, the maximum winding becomes fixed as indicated previously. This does not preclude the possibility of obtaining a winding which is apparently better than the maximum winding if the slot area for the starting winding is allowed to vary. This point is illustrated in the discussion of the table of results which is presented later.

This conclusion is based on a fixed distribution for the starting winding. In case the total area given by equation 21 is too large, can any other distribution result in a satisfactory maximum winding which requires less slot area? To investigate this, it is first necessary to derive the expression for the slot area required for the largest starting winding coil.

Circular mils of copper for the fullest starting winding coil

$$= [0.87K_{WM}^2C_M^2/(r_{1M}+Z_M)][l_e/K_{WS}^2S'] \tag{22}$$

where S' is the equivalent number of full slots for the starting winding. (Thus, if a four-pole 36-slot motor had a winding distribution for the starting winding of two 100 per cent coils and two 50 per cent coils under each pole, S' would be 24.) The terms in brackets are all functions of the distribution. If it is assumed that Appleman's[3] distributions for sinusoidal space waves are used, the bracketed quantity varies from 1.20 to 0.70 for the range investigated. The smaller quantity resulted from a wider distribution. Hence, we can conclude that, as more coils per pole are used, the maximum winding required less slot area.

Vector Relationships

The starting torque varies directly as the sine of the time angle between the starting and main winding currents. Refer to Figure 1. From equations 16 and 17, it is obvious that

$$\operatorname{Sin}\theta_t = \frac{X_M}{\sqrt{2Z_M(Z_M+R_M)}} \tag{23}$$

$$\operatorname{Cos}\theta_t = \frac{Z_M+R_M}{\sqrt{2Z_M(Z_M+R_M)}} \tag{24}$$

Since these terms are all functions of the main winding, it follows that for all maximum windings the angle θ_t is fixed. Then as r_{1S} and a^2 are varied (keeping the same product), the point p moves from $p_1=0$ to $p_2=\infty$ as in Figure 2.

Starting Currents

The total starting current is:

$$I_T = \sqrt{(I_S \sin\theta_t)^2+(I_S\cos\theta_t+I_M)^2} \tag{25}$$

or in terms of winding constants for maximum windings:

$$I_T = V\sqrt{\frac{a^4}{2Z_M(Z_M+R_M)}+\frac{1+a^2}{Z_M^2}} \tag{26}$$

The current in the starting winding is:

$$I_S = \sqrt{I_T^2-I_M^2\sin^2\theta_t}-I_M\cos\theta_t \tag{27}$$

Since each maximum winding will require a different total starting current and this current is limited usually by design specifications, it is useful to determine the ratio of transformation which will yield a required number of starting amperes. This can be obtained by solving equation 26 for a^2 Thus:

$$a^2 = \sqrt{2Z_M(Z_M+R_M)}\times\frac{\sqrt{I_T^2-I_M^2\sin^2\theta_t}-I_M\cos\theta_t}{V} \tag{28}$$

A simpler method of determining a for a given starting torque is shown later.

Starting Torque Per Ampere

Faced with a limit on starting current and a starting torque value which must often be kept high, the starting torque per ampere of starting current is an important figure. For all of the maximum windings which the designer can apply to a given job, the question naturally arises as to whether any proposed winding is producing as much starting torque as is possible for the limiting current. For that reason an investigation of the conditions for maximum starting torque per ampere is of interest.

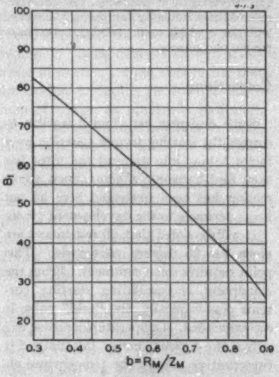

Figure 3. Constant for calculating starting torque

Equation 18 expresses the torque obtained from a maximum winding. For simplicity, the square of the total starting current will be used in setting up the ratio. Then from equations 18 and 26

$$\frac{\text{St.T}}{I_T^2} = \frac{112.7r_0a}{\text{Syn.rpm}}\cdot\frac{2X_M}{2(Z_M+R_M)(1+a^2)+Z_Ma^4} \tag{29}$$

When this is differentiated with respect to a and the result equated to zero, the maximum value for equation 29 is obtained. For this condition:

$$a^2 = \frac{(Z_M+R_M)+\sqrt{(Z_M+R_M)^2+6Z_M(Z_M+R_M)}}{3Z_M} \tag{30}$$

Working Curves

The formulas given up to this point can be simplified and the results plotted into useful curves by employing the ratio of constants R_M/Z_M. Let

$b = R_M/Z_M$

Then equation 18 becomes

$$T = I_M^2\frac{ar_0}{\text{Syn. rpm}}\left[\frac{112.7\sqrt{1-b^2}}{1+b}\right]$$

The quantity in brackets is plotted for a range of b values and is designated B_1. This is of value in determining the starting torque after a has been selected. See Figure 3.

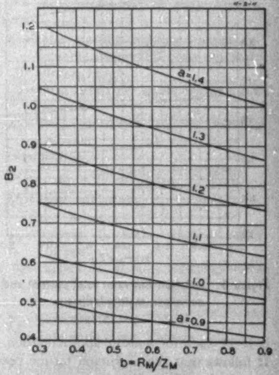

Figure 4. Relation between current in starting and current in main windings

The ratio of currents in the two windings:

$I_S/I_M = Z_Ma^2/\sqrt{2Z_M(Z_M+R_M)}$

If the b ratio is used, this can be expressed:

$$I_S/I_M = \left[\frac{a^2}{1.41\sqrt{b+1}}\right]$$

The quantity in brackets will be designated B_2 and is plotted for a range of values of b in Figure 4. This is useful for determining the starting winding current after a has been selected.

To determine the ratio of transformation required to produce a given total current the following ratio is useful:

$$I_T/I_M=\sqrt{\frac{a^4}{2(b+1)}+1+a^2}$$

The right-hand member, designated B_3, is plotted in Figure 5.

The equation for the ratio of transformation which gives the maximum torque per ampere squared can be rewritten:

$$a=\left[\sqrt{\frac{(1+b)+\sqrt{(1+b)^2+6(1+b)}}{3}}\right]$$

The quantity in brackets, designated B_4, is plotted in Figure 6. This is so nearly a straight line that we can assume it to be such for a useful approximation. It follows that the maximum torque per ampere is obtained for any maximum winding if the ratio of transformation is:

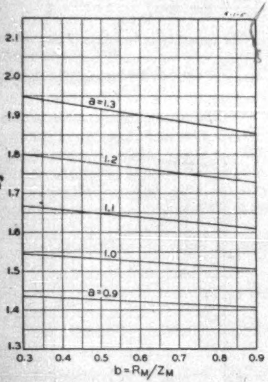

Figure 5. Relation between total current and current in main winding

$$a=1.12+0.3(R_M/Z_M)$$

Procedure

In the process of designing the starting winding for split-phase motors it is suggested that the curve data be used in the following manner. The main winding has been determined, and the constants have been calculated. Perform the operations listed.

(*a*). $I_M=V/Z_M$, $b=R_M/Z_M$.

(*b*). Select the starting winding distribution.

(*c*). Calculate K_{WS}, l_c, and S'.

(*d*). As a check, determine if the slot area provided for the starting winding is sufficient for a maximum winding (equation 22).

(*e*). If the starting current is the critical factor, use the allowable I_T and calculate I_T/I_M. Read the allowable value of *a* from Figure 5 which will yield this current. The *a* used can be less but should not be more than this value.

(*f*). For the chosen value of *a*, calculate the starting torque from

$$T=I_M{}^2\frac{ar_0}{\text{Syn. rpm}}B_1$$

(*g*). If the current and torque are satisfactory, determine the starting winding conductors,

$$C_S=\frac{K_{WM}C_M}{K_{WS}a}$$

(*h*). Determine the nearest wire size to yield r_{1S}.

(*i*). Calculate I_s from I_MB_2 of Figure 4; determine circular mils per ampere.

(*j*). Investigate if desirable, the effectiveness of the winding by comparing the *a* used with the *a* which would yield the maximum torque per ampere.

Corrections

Some designers may find it feasible to correct the starting torque equation by a factor taking into account the rotor bar skew and harmonic effects. Such a correction has been neglected here as not being germane to the development given.

It will be noted that all resistances are given for 25 degrees centigrade. This point requires some comment. From the standpoint of calculating starting currents pessimistically, the "cold" value is recommended. Temperature increase will reduce the current; hence, this method is conservative. As to the temperature effects on starting torque, an increase in resistance may reduce the current and the starting torque enough to yield less torque than desired. Temperature increase of the rotor and the starting winding may be opposite in their effects. Rapid heating of the starting winding usually occurs; it is practically impossible to predetermine with any accuracy the temperature at which starting winding and rotor winding will be operating by the time several starting torque tests have been taken. There is also some evidence that a slight skin effect is present in the rotor bars, giving a higher apparent resistance. All of these factors cause discrepancies between calculated and test values. In general, experience indicates the extent of the mental reservations with which the designer must examine calculated performance.

Typical Values

In the usual design of 60-cycle single-phase motors, the ratio of resistances to total leakage reactances is somewhat as follows:

$r_{1M}/X_M=0.3$ to 0.4 $r_0/X_M=0.3$ to 0.4

Substituting these values, we find that $(r_{1M}+Z_M)$ usually will be from 1.56 to 1.75 times X_M. For maximum windings this fixes the angles within limits, yielding values as shown in the following:

$r_{1M}/X_M=r_0/X_M=0.3$	$r_{1M}/X_M=r_0/X_M=0.4$
$\theta_M=59°2'$	$\theta_M=51°20'$
$\theta_S=30°50'$	$\theta_S=26°17'$
$\theta_t=28°12'$	$\theta_t=25°3'$
$\theta_s/\theta_M=0.52$	$\theta_s/\theta_M=0.51$

Example: A starting winding is to be designed for a motor rated at one-eighth horsepower, 60 cycles, 1,150 rpm, 115 volts. The following constants are known:

$r_0=r_2K_r=4.85$ $Z_M=14.1$
$r_{1M}=4.41$ $b=0.657$
$R_M=9.26$ $r_{1M}+Z_M=18.51$
$X_M=10.7$ $I_M=\dfrac{V}{Z_M}=8.17$
$C_M=1{,}248$
$K_{WM}=0.856$

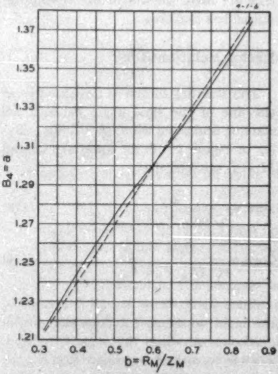

Figure 6. Ratio of transformation required for maximum torque per ampere squared

The starting winding is to be designed to yield a starting torque of 15 ounce-feet without exceeding 15 amperes of starting current. (Lettered items identify the steps previously given in the procedure.)

(*b*). A starting winding distribution will be selected for which

(*c*). $K_{WS}=0.837$ $l_c=4.45$ $S'=24$ slots

(*d*). From equation 22, it is seen that 14,200 circular mils of slot area might be needed. Adequate space has been provided in the slot so that the fullest starting winding coil can have this value of useful copper cross section if required.

(*e*). $I_T/I_M=15/8.17$ or 1.85

From Figure 5

$a=1.27$

This is the ratio of transformation which will yield a starting current of 15 amperes.

(*f*). Read B_1 from Figure 3 as 51.2.

$$T=8.17^2\,\frac{1.27\times 4.85}{1{,}200}\cdot 51.2 \text{ or } 17.5 \text{ ounce-feet}$$

Since only 15 ounce-feet of starting torque are required, some leeway can be used in the number of turns (reducing rather than raising *a*) and in the starting winding wire size. If need be, *a* could be as low as 15/17.5×1.27 or 1.09.

(*g*). The starting winding conductors:

$$C_S=\frac{0.856\times 1{,}248}{0.837\times(1.27 \text{ to } 1.09)}=1{,}010 \text{ to } 1{,}170$$

Conductors in the fullest slot will be:

$$C_S/S'=\frac{1{,}010 \text{ to } 1{,}170}{24}=42 \text{ to } 48.7$$

The minimum value yields the highest starting torque.

(*h*). The wire size can be determined readily by several different methods, but, since item *d* has shown that the circular mils per slot should be 14,200, the circular mils per conductor are obviously:

14,200/(42 to 48) = 349 to 295

Use number 25 with a cross section of 320 circular mils.

This will yield a starting winding resistance of from 12.1 to 14.0 ohms, depending upon the number of turns used. To satisfy the relationship of equation 14 the resistance should fall between 11.4 to 15.5 ohms with the variation in turns. We might assume that 44 turns will be used in the fullest coil, giving C_S a value of 1,056. Then $a=1.21$ and $r_{1S}=12.7$.

Since

$r_{1S}=1/a^2(r_{1M}+Z_M)$

is still satisfied by this choice, the final winding is a maximum winding. Some leeway usually can be tolerated in this final check.

(*i*). The starting winding current:

$I_S=I_MB_2=8.17\times 0.80$ or 6.53.

B_2 is read from Figure 4. The circular mils per ampere = 320/6.53 or 49.

(*j*). To check the design against the maximum starting torque per ampere, use B_4 in Figure 6, or calculate. Then $a=1.12+0.3\times 0.657$ or 1.317. From this viewpoint the winding could be more effective.

The final value of starting current is 14.4 amperes, yielding a starting torque of 16.8 ounce-feet.

Conclusions

The advantages and disadvantages of this design procedure are well illustrated by the numerical example and table of results. The use of the maximum-winding method and the accompanying curves yield at once the desired performance items. The tabulated results of the sample design are shown in column 1 of the table. As a further investigation the resistance of the starting winding was arbitrarily increased for the calculations of column 2, and decreased for the calculations of column 3. Both changes showed a decrease in starting torque, illustrating that the maximum winding yields a true maximum of torque as previously indicated. Yet the actual change in torque is small, which indicates that the curve of torque versus starting winding resistance, under the other conditions maintained in this method, is comparatively flat. This is a useful fact, for it gives the designer some choice in starting winding conductor cross section without serious interference with final results.

As pointed out previously, many combinations of windings might yield the desired results. An example of this is seen in column 4. By trial and error, with the use of a different ratio of transformation, a winding was obtained to satisfy the limiting conditions, even though it was not a maximum winding. When the same number of conductors was adjusted in resistance until it resulted in a maximum winding, the design specifications were no longer met. These results are shown in column 5. It should not be concluded that the calculation of another winding to yield the desired results upsets the advantages of the procedure shown here. Such a winding was obtained only by a lengthy trial-and-error process, with each step calculated without the benefit of the labor-saving curves resulting from maximum winding procedure. Furthermore, it will be noticed that such a winding is obtained without regard to the conditions set up as to the slot space to be utilized for starting winding conductors.

Table I. Contrasted Results

	1	2	3	4	5
Conductors, starting winding	1,056	1,056	1,056	850	850
Ratio *a*	1.21	1.21	1.21	1.5	1.5
Resistance, starting winding	12.7	16.0	10.0	12.7	8.22
Starting torque, ounce-feet	16.80	16.10	16.15	19.2	20.6
Total starting amperes	14.4	13.3	15.45	13.96	17.9
Starting winding amperes	6.53	5.55	7.55	7.4	10.13
Torque per ampere	1.165	1.208	1.042	1.286	1.15
Circular mils per ampere	49	47	55.4	34.9	39.6
Comment	Maximum winding	r_{1S} too large	r_{1S} too small	New *a* trial design	New *a* maximum winding

List of Symbols

r_{1M} = the resistance of the main stator winding

r_{1S} = the resistance of the starting winding

r_2 = the rotor resistance in main winding terms

r_0 = the corrected rotor resistance in main winding terms $=K_rr_2$

$R_M=r_{1M}+r_0$

$R_S=r_{1S}+r_0/a^2$

a = ratio of transformation, main to starting windings

X_M = total main winding leakage reactance, stator and rotor, in main winding terms

X_S = total starting winding leakage reactance, stator and rotor, in start winding terms

Z_M = total impedance, main winding and rotor

Z_S = total impedance, starting winding and rotor

$K_r=\dfrac{X_0-X_M}{X_0}$ (all main winding terms)

X_0 = total magnetizing and primary leakage reactance

I_M = current in main winding, rotor blocked

I_S = current in starting winding, rotor blocked

I_T = vector sum of I_M and I_S

θ_M = time angle of lag, V and I_M

θ_S = time angle of lag, V and I_S

θ_1 = time angle, I_M and I_S

C_M = series turns, main winding

C_S = series turns, starting winding

K_{WM} = winding factor, main winding

K_{WS} = winding factor, starting winding

l_c = average length of starting winding conductor, in inches

A = cross section of conductor in circular mils

S' = effective number of full stator slots, using the largest number of turns in any starting winding coil as 100 per cent

$b=R_M/Z_M$

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Some Aspects of Electric-Motor Design—Polyphase-Induction-Motor Design to Meet Fixed Specifications

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A CURIOUS gap exists in the technical literature dealing with electrical-design methods. Procedures for determining performance by various analyses and for various testing methods can be found on nearly all of the more common machines. Yet the novice or the intelligently curious, interested in the field of design, finds but few references in the technical literature concerning the procedure whereby the machine dimensions were first determined and the completed design made. While it is true that a comparatively small group of engineers may be concerned with such design work, the philosophy and technique of approach could be of interest to an entire profession which is so profoundly influenced by the designer's work.

It is the purpose of this paper to examine the task of the electrical designer and the modern approach to his problems. Because the field is too large for generalities to be understood readily in their complete significance, the polyphase induction motor, only, will be considered. This field is covered in print, to a limited extent, by reference or text books on design. The point of view, so presented, will first be analyzed to determine how adequately it meets the requirements of the design engineer, and an additional approach will be suggested. This is in no way intended as a minute and detailed exposition of electrical design but rather a point of view from which the designer operates, with illustrative examples.

The first step in considering the design of a polyphase motor is a matter of definition. A motor has a certain rating because

1. At that rating, the breakdown torque is at least a certain minimum percentage of the full-load torque.
2. The starting torque is a certain minimum percentage of the full-load torque.
3. The locked-rotor amperes do not exceed a certain definite value.
4. The full-load temperature rise does not exceed a definitely fixed value.

For general-purpose motors or some groups of special motors, these specifications are fixed by standardization. However, no recent standards are available on item 3 aside from the requirements of power companies, and the old National Electric Light Association rules, which appear to be considered rather generally by the motor industry.

Only the addition of limits on full-load slip are needed to complete the list of important motor characteristics which must be met by a successful design. Even with no standards at present pertaining to this item, it is limited largely through having to deal with a competitive market. The same might be said of practically all items concerning motor performance. The type of application, the specifications set by the customer, or the general understanding as to what is acceptable commercial practice are all of such importance that, even with no standardization on any of these items, in effect they are all fixed either exactly or between limits.

With known values of these characteristics required of the completed design it would appear that a satisfactory design procedure would incorporate methods whereby the characteristics could be achieved. It is interesting at this point to examine the process most commonly published for initiating a design, namely, the use of the D^2L formula. This takes a form about as follows:

$$D^2L = \frac{\text{horsepower}}{\text{rpm}} \times \frac{K}{B_g \times \text{power factor} \times \text{efficiency} \times q}$$

wherein

D = rotor diameter in inches
L = axial length of the rotor stack in inches (assumed equal to the stator stack)
K = a constant
B_g = flux density in the gap
q = ampere conductors, stator or rotor, per inch of periphery

This formula is theoretically correct. For any given design, if the correct values are substituted in the right-hand member, it yields a calculated value of D^2L in agreement with the physical facts. But it is when the process is applied in the form for which it is intended that some ambiguity exists. Consider the terms making up the formula.

The average gap density, B_g, may fall between limits of 30,000 to 40,000 lines per square inch for 60-cycle motors, according to various writers. Actually this may be influenced by a number of factors. These would involve the proportion of tooth and yoke areas in the stator and also the rotor laminations. The radial length of air gap has some influence also, for higher gap densities are usually avoided on motors in which the stator bore is not ground after construction. These all influence magnetic side pull and hence reduce the allowable gap density. In light of these facts, considerable variation in the density is logical and inevitable.

The full-load power factor and efficiency are required in the equation. These are also terms which are subject to some variation, although not over so great a range. Suppose we select a 25-horsepower, 1,725-rpm general-purpose motor as typical. From different sources we obtain values as follows:

Power factor	0.900	0.89	0.958	0.935	0.87	0.890
Efficiency	0.905	0.90	0.910	0.890	0.89	0.895
Apparent efficiency	0.8145	0.801	0.8718	0.832	0.774	0.7965

These represent two published sources and four tests of different manufacturers' motors.

The remaining term, q, involving ampere conductors, is one of the chief causes for uncertainty in this equation. It is related to circular mils per ampere and may appear in conjunction with other terms as "electric loading." Ultimately, it is intended to give a measure of heating or to control the temperature rise of the motor. Now if the D^2L equation is expected to be widely useful, it is to be assumed that considerable data on probable values of q for standard open motors,

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enclosed motors, intermittent ratings, and so forth should be available. Actually, suggested values are nonexistent except for a few average curves.

It was approximately six years ago, that the writer's interest in the D^2L equation and the q factor was aroused to the extent of starting an active file on the subject. Since then, the calculated values of q from various motors, representing a wide range of manufacturers, have been accumulated. The results have been both astonishing and worthless. Even though such types as totally enclosed, totally enclosed fan-cooled, intermittent ratings and 25-cycle designs are segregated, no possible curves for suggested values of q versus horsepower could be drawn through the several hundred points obtained. One cause of discrepancy is the fact that while one group of motors was nominally 40 degrees centigrade rise, the actual temperature rise at full load for such motors varied from 19 degrees centigrade to 45 degrees centigrade. Typical values for the 25-horsepower rating previously mentioned were 635-, 790-, and 885-ampere conductors per inch for three motors, against 500 and 710 from reference-book recommendations.

More must be said about electric loading, but let us first take stock of the influence of these factors. It must be kept in mind that the whole purpose of the traditional design process is to assume reasonable values for these factors, to substitute in the D^2L equation and thereby to determine the armature dimensions for the given horsepower and speed. Let us consider the 25-horsepower rating, determine D^2L, using the lowest values of the factors, and recalculate, using the highest values of all terms as recommended or found on actual designs. Granted that this may be an extreme procedure, the results are none the less surprising. The designer is thereby offered a choice of approximately 2.6 to 1 for the rotor volume. In short, if he has selected say ten inches for the rotor diameter, the D^2L equation has "fixed" the lamination stack between approximately 2.5 to 6.0 inches. One does not need to be a very experienced designer to be able to estimate this closely and avoid the nuisance of calculation.

Having gone through the calculation of the required D^2L for a given horsepower and speed, it is well to re-examine those first items by which the motor has been defined. To repeat, a motor has a certain rating only because its maximum torque, its starting torque, its starting amperes, its temperature, and its slip are within certain limits. Yet one finds no such items directly in the D^2L process, and one is forced to the conclusion that this design approach has initiated a design procedure with no regard to the items by which the design is defined. Several more subtle difficulties are present. When q has been chosen for the design calculations, the number of conductors on the winding are thereby fixed, except for the possible changes of D relative to L. Working with a selected value of flux density and a relatively fixed number of ampere conductors per inch may fix the maximum torque but with no regard to the desired value. The final design may readily be one in which the chosen proportions actively prevent the designer from obtaining a characteristic demanded of the motor.

Of all the terms in the D^2L equation, only one is assumed to be concerned directly with a desired characteristic. This is q, which, as a measure of electric loading, is intended to assure a safe value of temperature rise. Unfortunately, experience tends to minimize the importance of electric loading rather than emphasize it. *Motor ventilation introduces an important factor which is taken into account in no design process.* Yet its effect is equally important, if not more important, than electric loading.

The design office with which the writer is connected has just completed a new series of motors in which a thorough investigation has been made of motor air delivery, velocity through the ventilating ducts, and air-flow paths. (All attendant heat runs were made by thermocouples, located at various positions throughout the winding and on the core surface.) Conclusions, extreme as they may appear, can be summed up about as follows: A design using say 300 circular mils per ampere in the stator winding, with correct ventilation, may display lower temperature rise than one with 600 circular mils per ampere and ineffective ventilation. The correct design of a cooling fan, or of baffles used with the fan may have more effect on motor heating than a 75 per cent or even 100 per cent change in electric loading. On totally enclosed motors the use of properly located fans and in some cases fins to break up the vortex caused by these fans, may make a difference of from 20 to 30 degrees centigrade on the temperature of the stator copper.

If the classical theory for initiating a motor design is inadequate for meeting the specifications of a competitive market, by what method is a motor designed? Before attempting an explanation, it is informative to look at the next steps which tradition has given us for the design procedure. These are

1. The design of the laminations and windings, using the rotor diameter and stack as determined by the D^2L equation.
2. The calculation of winding constants.
3. The determination of performance.

Note in this process that the defining items, determining the success of the design, cannot be calculated until the design is finished. Failure calls for extensive redesign. Let us modify this traditional point of view, setting up a new criterion for a successful design method, namely, an ideal method for design is one built around a set of equations in which it is possible *to supply the desired characteristics* and solve for the physical constants. Note that this is a direct approach to the defining items.

Following this procedure, a polyphase induction motor could be designed by selecting any one of the defining characteristics as a means of determining motor proportions and windings. But the process is greatly simplified if these are fixed by maximum torque.

The maximum torque is influenced by only two of the winding parameters, stator winding resistance, and total leakage reactance. Once it is used to determine these winding parameters, in conjunction with the rotor resistance, it fixes

(*a*). Locked-rotor amperes.
(*b*). Starting torque.
(*c*). Full-load slip.

For these reasons the design process will be set up on the assumption that motor proportions and stator winding are fixed by the required maximum torque of the motor.

An electric motor consists of both a magnetic and an electric circuit. Theoretically, both need to be designed; actually, in the great majority of cases the contour (if not the area) of the magnetic circuit is fixed. This is because the motor industry may sell horsepower but actually sets up to manufacture frames. Into these frames certain laminations can be built, and once the dies for producing them have been made, economy demands as wide a use of these laminations as possible. Changes in winding specifications represent no such investment, and hence the designer is constantly under the economic pressure to build as many ratings as practicable in the same laminations, achieving the desired characteristics by control of the electric circuit.

For these reasons the first example of design process shown here will be based on the assumptions that the laminations are already designed and that the stack has been chosen. In short, this is design in a fixed magnetic circuit, using a predetermined or fixed radial length of air gap.

The Maximum-Torque Equation

The equations to be used in these analyses will be based entirely on the approximate equivalent circuit for the three-phase induction motor. The writer does not consider them accurate enough for high-grade design work, but in this paper we are interested in the principles involved and do not want to complicate the approach by a series of corrections necessary to the concepts of the exact circuit representation.

On such a basis, a common equation for maximum torque is:

$$T_m = \frac{7.05}{\text{Syn. rpm}} \frac{nE^2}{2[R_1+\sqrt{R_1^2+X^2}]} \quad \text{pound-feet} \qquad (1)$$

The first requirement is to obtain, not torque in terms of circuit constants, but circuit constants in terms of torque. Then

$$R_1+\sqrt{R_1^2+X^2} = \frac{7.05nE^2}{2(\text{Syn. rpm})T_m}$$

There are two winding constants in this equation. It is difficult to obtain a winding in terms of both R_1 and X, and for simplification we will make use of the ratio α, defined as R_1/X. Now

$$T_m = \frac{7.05nE^2}{2(\text{Syn. rpm})X}\left[\frac{1}{\alpha+\sqrt{\alpha^2+1}}\right] \quad \text{pound-feet} \qquad (2)$$

or

$$X = \frac{3.52nE^2\beta}{\text{Syn. rpm } T_m} \qquad (3)$$

where

$$\beta = \frac{1}{\alpha+\sqrt{\alpha^2+1}} \qquad (4)$$

There are two facts connected with this transformation which are highly important.

1. Through equation 3 we obtain an expression for the reactance of the winding to yield a required value of maximum torque.
2. This expression no longer contains the term R_1 but merely the ratio of R_1/X.

The ratio of stator winding resistance to total leakage reactance is independent of the winding for a fixed stack. This ratio is a function of the lamination design and is constant except for the slight change brought about through winding pitch. It is upon this fact that the entire simplification of the design process depends.

It is now only necessary to obtain an expression for leakage reactance in terms of stator conductors to determine directly the winding necessary for a required value of maximum developed torque.

To follow these steps in more detail:

Resistance of the Stator Winding. If the area of each stator slot is A circular mils, of which the decimal fraction k is useful, the total cross-section area of copper available per phase is

$$\frac{AkS_1}{n} \quad \text{(circular mils)} \qquad (5)$$

If l_c is the average length of conductor, the resistance of all copper per phase, with the slots full and all of the conductors in parallel, is

$$\frac{l_c n}{AkS_1} \quad \text{(ohms)} \qquad (6)$$

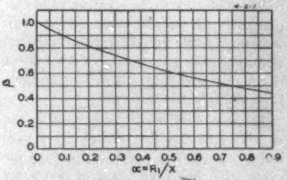

Figure 1. Correction for stator resistance drop

Split up into C series conductors, the resistance of the winding per phase at 60 degrees centigrade will be

$$R_1 = \frac{l_c n}{AkS_1} C^2 \qquad (7)$$

Leakage Reactance, Stator and Rotor. The form to be used for this equation is

$$X = 2\pi f C^2 n \mathcal{P} 10^{-8}$$

wherein $\mathcal{P}$ is the permeance of the leakage paths for both stator and rotor, corrected for pitch.

The equation

$$\frac{R_1}{X} = \frac{l_c \times 10^8}{2\pi f AkS_1 \mathcal{P}} \qquad (8)$$

is the development of the statement made previously concerning this ratio. The obtaining of an expression for winding resistance as a function of C^2 is shown to be useful by the fact that this eliminates the number of conductors from the ratio, leaving it (as previously mentioned) as a function of the laminations. When R_1/X is known, it is convenient to obtain β by the curve of Figure 1.

If this is substituted in equation 3, we obtain a value for X, from which it follows that the required series conductors are

$$C = \sqrt{\frac{X \times 10^8}{2\pi f n \mathcal{P}}} \qquad (9)$$

The total series conductors Cn will be divided into S_1 slots. The approximate copper area per slot is Ak, and hence the circular-mil area of each conductor is

$$\frac{AkCn}{S_1} \qquad (10)$$

This is adjusted to the nearest wire size and the number of parallel paths desired.

We now have the stator winding, both turns and wire size, to give a prescribed maximum torque. When the rotor winding has been calculated, it represents a completed design, except for all of the performance checks which a careful designer may wish to apply. Only one such check will be shown at this point.

Flux Density in the Stator Teeth (Maximum) in Kilolines Per Square Inch.

$$B_t = \frac{70.9EP10^8}{C \times f \times k_w \times t_w \times L \times S_1} \qquad (11)$$

Note that the weak point in this method lies chiefly in the fact that the initial steps in design are taken without regard to flux density. Only when the conductors have been determined can the density be checked. This is not often serious for an experienced designer, but it is confusing to the novice. The next step shown eliminates such uncertainty.

Stack Necessary to Produce Required Torque and Density

In the previous analysis the stack was fixed, and the flux density for the required design was determined only as a final check. A method will now be shown whereby the stack and winding will be determined, simultaneously, to yield the required torque in a given set of laminations. The problem is complicated by the fact that there exists no simple relationship between maximum torque and stack for a fixed flux density. Note that the maximum torque depends upon both R_1 and X. As the stack varies, R_1 becomes a function of a changing length of conductor, one portion of which (the embedded part) varies as the stack; the end-coil length is fixed for all stacks. Also, as the stack varies, two components of X (the slot and zigzag components) vary directly, while the end-leakage component remains independent of stack.

The resultant variation of torque with stack depends upon the relative values of these components in any design.

Now if it were possible to divide an electric motor into two imaginary parts, one made up only of the stacking and the constants resulting from the embedded conductors, and the other made up of the winding constants resulting only from the end coils, the former part would yield a torque as a simple function of stack. It would then be possible to vary the stack until the required flux density was achieved, adding the influence of the "end" motor to obtain the over-all performance.

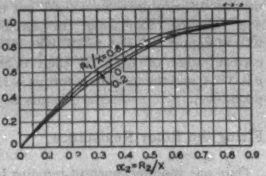

Figure 2. The ratio *a* represents starting torque divided by maximum torque

A polyphase induction motor can be so divided, and we can write:

For a fictitious "stack" motor, having no end coils

$$T_{ms}=\frac{7.05nE^2\beta_s}{2(\text{Syn. rpm})X_s} \tag{12}$$

where X_s is made up of slot and zigzag leakage components only; β_s is the relation of equation 4 resulting from the embedded portion of the conductors.

For a fictitious "end" motor

$$T_{me}=\frac{7.05nE^2\beta_e}{2(\text{Syn. rpm})\ X_e} \tag{13}$$

where X_e is the end leakage only, β_e is the relation shown in equation 4, using end leakage and resistance of end coils only.

The actual maximum torque of a motor in terms of the two fictitious components is

$$T_m=\frac{T_{ms}T_{me}}{T_{ms}+T_{me}} \tag{14}$$

This relationship is useful because, when expanded, it can be solved directly for stack. As a first step, from equation 14

$$T_m=\frac{3.53nE^2\beta_s\beta_e}{(\text{Syn. rpm})(X_e\beta_s+X_s\beta_e)}$$

To expand to permeances

$$2\pi fC^2n10^{-8}[\mathcal{P}_e\beta_s+\mathcal{P}_s'L\beta_e]=\frac{3.53nE^2\beta_e\beta_s}{\text{Syn. rpm}\times T_m} \tag{15}$$

When the expression for stator tooth density shown in equation 11 is used, it follows that the required number of conductors are

$$C=\frac{1}{L}\left[\frac{EP}{B_tf}\cdot\frac{70.9\times10^8}{S_1t_wk_w}\right] \tag{16}$$

Let the expression in brackets be C_o and substitute in equation 15:

$$\frac{\mathcal{P}_e\beta_s}{L^2}+\frac{\mathcal{P}_s'\beta_e}{L}-\frac{3.53E^2\beta_s\beta_e\times10^8}{2\pi f(\text{Syn. rpm})C_o^2T_m}=0 \tag{17}$$

To solve for L

$$L=\frac{1}{\sqrt{Q^2+Q_1}-Q} \tag{18}$$

wherein

$$Q=\frac{\mathcal{P}_s'\beta_e}{2\mathcal{P}_e\beta_s} \tag{19}$$

$$Q_1=\frac{3.53E^2\times10^8}{(\text{Syn. rpm})2\pi fC_o^2T_m}\cdot\frac{\beta_e}{\mathcal{P}_e} \tag{20}$$

This value of L is the stack required to give the desired maximum torque, maintaining the desired flux density in the teeth. The series conductors per phase are then determined from C_o/L. If this process appears too complicated, it should be kept in mind that many of the terms used are constant for the laminations and, if recorded with them, need not be recalculated for each design.

We have now reached the same point achieved in the first process and, with the stator winding fixed (by either of the two methods), are ready to complete the design.

Examples

The ideas expounded in the preceding material will be illustrated by an example. The problem is to fix the design of a general-purpose motor rated as three phase, 60 cycles, four poles, 25 horsepower, 220 volts.

The specifications to be met are:

Maximum torque, 210 pound-feet or 275 per cent of full load.

Starting torque, 122 pound-feet or 160 per cent of full load.

Starting kilovolt-amperes per horsepower 4.5 to 5.6.

Full-load slip, not over five per cent.

Temperature rise, 40 degrees centigrade continuous.

The frame in which this motor is to be built has a diameter such that it will accommodate laminations with an outside diameter of 14.75 inches. In effect this is fixed by the fact that the standard National Electrical Manufacturers Association frame for 25-hp 1,750-rpm rating has a mounting height of shaft above base of nine inches, thereby limiting motor diameter to 18 inches. Actually only a lesser dimension is available, still further reduced by the thickness of the shell or frame casing. Of several laminations which might be available, the one selected displays the following data:

Stator

Outside diameter	14.75 inches
Bore	10.0 inches
Slots	48
Tooth width	0.3125 inch
Slot area	0.51 square inch
Slot constant	1.89

Rotor

Outside diameter	9.94 inches
Slots	60
Minimum tooth width	0.182 inch
Slot constant	1.3

Winding factor, for throw of coils (slots 1 to 11), is $k_w=0.925$.

The first step in the theory involved design in a fixed stack. We will assume at this point that a stack of five inches will be satisfactory. Later the work will be repeated, solving for both stack and winding.

From the data we calculate the following permeances for an assumed pitch of 83.3 per cent. These are the sum of both stator and rotor values.

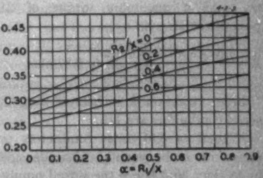

Figure 3. Locked volt-amperes equal *b* times the product of maximum torque and synchronous rpm

Slot permeance	=0.99
Zigzag permeance	=0.655
End permeance	=0.764
Total leakage permeance	=2.409

Note that the leakage reactances dealt with are slot, zigzag, and end components for both stator and rotor. This method can be used regardless of the leakage components preferred. Thus, one may choose to deal with slot, differential, end, and skew components, as calculated by formulas generally available. It is only necessary to modify these formulas

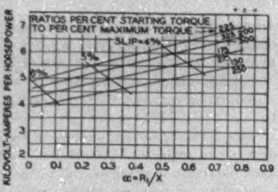

Figure 4. Relation between locked kilovolt-amperes per horsepower, starting torques, maximum torques, and slip for various values of R_1/X

(regardless of their source) until they take the form

$$X\text{(any component)} = 2\pi f n \mathcal{P} 10^{-8}$$

The permeance $\mathcal{P}$ must be corrected for pitch, using such methods as given by Adams or Alger.

To calculate the stator resistance we know that the slot area is 650,000 circular mils of which about 28 per cent can be filled with useful copper.

The length of conductor is calculated to be 16.95 inches, and there are 16 slots per phase. Therefore, the ratio of R_1/X for these laminations is

$$\frac{16.95\times10^8}{2\pi f\times650{,}000\times0.28\times48\times2.409} \text{ or } 0.213$$

By referring to the curve of Figure 1 we find that β equals 0.815, and hence we know that the leakage reactance required to yield a maximum torque of 210 pound-feet is

$$X = \frac{3.53\times3\times127^2\times0.815}{1{,}800\times210} \text{ or } 0.37 \text{ ohm}$$

From equation 9 it follows that the series conductors are 116.

A total of 116 series conductors per phase cannot be wound into similar coils in 48 slots, and hence the actual number used will be 112 conductors resulting from 14 turns per coil.

As the useful slot area is 182,000 circular mils, the area per conductor is 6,500 circular mils requiring the use of number 12 wire in a four-wye circuit.

The stator winding is now fixed both as to number of turns and wire size. The final correct value of R_1/X is still 0.213, X is 0.34, and the maximum torque is 227 pound-feet.

We should still check the flux density, using the stator tooth density as a criterion. From equation 11 we find

$$B_t = \frac{70.9\times127\times4\times10^8}{112\times60\times0.925\times0.3125\times5\times48} \text{ or } 77.2$$

This completes the first part of the design by the method outlined here. It has been a simple, direct process, giving at once a characteristic required by the specifications.

In case, however, that the experienced designer has not been able to estimate the required stacking to give such characteristics, we will illustrate the second method already discussed, whereby the stack and winding are both determined for the maximum torque.

Second Method

The same motor is to be designed with the same laminations, but we will assume at the start that a flux density of 86,000 lines per square inch in the stator teeth will be acceptable. (Values might vary from 75 to 100 kilolines.) The problem now is to determine the stack and the winding.

It is assumed that the following data, pertaining to the laminations as used previously, have been tabulated.

(*a*). The slot permeance (both stator and rotor) per inch of stack for various pitch windings. This will be 0.198 for a pitch of 83.3 per cent.

(*b*). The combined zigzag permeance per inch of stack for various pitch windings and with a fixed radial length of air gap. This will be 0.131 for a pitch of 83.3 per cent. The sum of slot and zigzag permeances will be designated at $\mathcal{P}_s'$ and equals 0.329 for this example.

(*c*). The combined end-leakage permeance for various winding pitches. For the example, this is 0.764.

(*d*). The resistance of a fictitious conductor, equal in area to the total cross-sectional area of all slots per phase, and one inch in length.

(*e*). The length of end connection of one conductor for various throw of coils.

(*f*). The resistance of a fictitious conductor, equal in area to that of item *d* but equal in length to the end connections of item *e*.

From such tabulated data it is necessary to determine R_1/X for the stack motor:

$$\frac{R_1}{X}\text{ (stack)} = \frac{10^8}{2\pi f A k S_1 \mathcal{P}_s'}$$

$$= \frac{10^8}{377\times180{,}000\times48\times0.329} \text{ or } 0.0935$$

R_1/X for the end motor is

$$\frac{l\text{(end)}10^8}{2\pi f A k S_1 \mathcal{P}_e} = \frac{11.95\times10^8}{377\times180{,}000\times48\times0.764} \text{ or } 0.478$$

Refer to Figure 1 and obtain

$$\beta_e = 0.63;\ \beta_s = 0.914$$

From equation 16

$$C_e = \frac{127\times4}{80\times60}\cdot\frac{70.9\times103}{48\times0.3125\times0.925} \text{ or } 540$$

Calculate

$$Q = \frac{0.329\times0.63}{2\times0.76\times0.914} \text{ or } 0.149$$

$$Q_1 = \frac{3.53\times127^2}{1{,}800}\cdot\frac{10^8}{377\times540^2\times210}\cdot\frac{0.63}{0.764} \text{ or } 0.103$$

From equation 18 $L = 4.56$ inches

This can be rounded off to 4.5 inches.

The series conductors per phase are C_e/L or 120.

This needs no adjustment, as it results in uniform coils of 15 turns each. Using number 12 wire provides a tight but usable winding, increasing the circular mils of copper per slot to 197,000. The correct value of R_1/X is then 0.207; β is 0.814, and the maximum torque is 212 pound-feet.

Fixing the Rotor Resistance

Only one winding constant remains for determination, namely the rotor resistance. This will determine in part the starting torque, the starting amperes (or starting kilovolt-ampere per horsepower) and the full-load slip. Since all of these characteristics are determined by the value of R_2 it is obviously impossible to obtain, say, certain values of starting torque and then expect to obtain, independently, values of slip or starting amperes. Their interdependence prevents such wide choice.

In the analyses which follow we will see how rapid checks on the practicability of the characteristics can be obtained readily and the rotor resistance chosen.

STARTING TORQUE

Using the approximate equivalent circuit, the starting torque is

$$T_s = \frac{7.05}{\text{rpm}}\cdot\frac{nR_2E^2}{(R_1+R_2)^2+X^2} \tag{21}$$

The ratio of starting torque to maximum torque will be

$$T_s/T_m = \frac{2R_2\left[R_1+\sqrt{R_1^2+X^2}\right]}{(R_1+R_2)^2+X^2} \tag{22}$$

If this is set up in terms of the ratios, $R_1/X=\alpha$ and $R_2/X=\alpha_2$, we obtain a relationship such that

$$T_s = aT_m \tag{23}$$

where

$$a = \frac{2\alpha_2\left[\alpha+\sqrt{\alpha^2+1}\right]}{(\alpha+\alpha_2)^2+1} \tag{24}$$

This is plotted in Figure 2.

Note that the first steps in this process have fixed R_1/X and the maximum torque. Hence, with the starting torque chosen, the use of these curves immediately fixes R_2/X and hence R_2.

STARTING KILOVOLT-AMPERES PER HORSEPOWER

Since this ratio gives a measure of the starting current, it is subject to power-company limitation. A series of bands has been set up as standard by NEMA each range being designated by a code letter to appear on the motor name plate. In general, the starting kilovolt-amperes have been fixed largely by the maximum torque, although the rotor resistance has some influence. The relationship can be derived as shown below:

$$\text{Volt-amperes (start)} = \frac{E^2 n}{\sqrt{(R_1+R_2)^2+X^2}} \qquad (25)$$

Ratio

$$\frac{Va}{T_m} = \frac{\text{rpm}}{3.53} \frac{\left[R_1+\sqrt{R_1^2+X^2}\right]}{\sqrt{(R_1+R_2)^2+X^2}} = \text{rpm}\left[\frac{\alpha+\sqrt{\alpha^2+1}}{3.53\sqrt{(\alpha+\alpha_2)^2+1}}\right] \qquad (26)$$

The value in brackets is plotted as b in Figure 3. Obviously

$$b \times T_m \times \text{rpm} = \text{starting volt-amperes}$$

Once R_2 has been chosen by starting-torque considerations, the value of b can be read to determine the volt-amperes at starting.

It is interesting to note that the relationship of equation 26 can be rewritten in the form:

$$b = 0.1835 \frac{\text{locked kva per hp}}{T_m/\text{full-load torque}} \qquad (27)$$

or

$$5.46b \times (\text{ratio of maximum to full-load torque}) = \text{locked kva/hp}$$

By selecting various useful ratios of maximum to full-load torques and starting to full-load torques, it is possible to assume a series of values of R_1/X, thereby fixing the required values of R_2/X. With these known, b can be calculated and the resulting kilovolt-amperes per horsepower determined. Such curves are shown in Figures 4 and 5. While only two sets are shown, they can be prepared readily for any range of values and form an extremely useful tool for the designer.

FULL-LOAD SLIP

This value is of special importance in many cases and obviously has been fixed as soon as the maximum torque and rotor resistance are determined. Any fairly accurate check on the probable value is of interest before the final design is completed. To obtain such check it is possible to make use of a derivation based on the following:

The slip at any torque is approximately

$$s = \frac{R_2}{Y+R_2} \qquad (28)$$

$$Y = \frac{E^2 n \times 5{,}250}{746 \times T \times \text{rpm}} - 2(R_1+R_2) \qquad (29)$$

Using the ratio of maximum to full-load torque, we obtain by substitution

$$Y = \frac{2\times(T_m/T)}{\beta} - 2(R_1+R_2)$$

From this it follows that

$$s = \frac{\alpha_2}{\frac{2T_m/T}{\beta} - 2\alpha - \alpha_2} \qquad (30)$$

Refer again to the curves of Figures 4 and 5. For each torque ratio chosen, both α_2 and α are fixed, thereby enabling the full-load slip to be determined through equation 30. A few of the slip curves have been added to these figures, and the value of such curves is apparent, as they indicate at once the limitations in design.

Example Continued

Before proceeding with the actual cage design, the required value of rotor resistance will be determined.

Note that a starting torque of 160 per cent is required and that R_1/X is 0.207. Reference to Figure 5 shows by interpolation that the slip would be slightly less than five per cent for these ratios and that the kilovolt-amperes per horsepower is approximately 4.68. These are within the limits set in the specifications. Hence the design is feasible. Had such values been different from the requirements, *no amount of readjustment would have made it possible to have obtained all of these characteristics.* It is often likely that impossible specifications can be written for simple squirrel-cage polyphase induction motors.

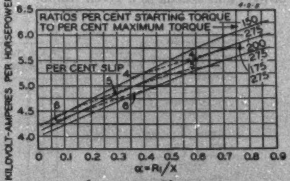

Figure 5. Relation of locked kilovolt-amperes per horsepower, starting torques, slip and various values of R_1/X for a fixed percentage of maximum torque

Calculating the ratio of T_s to T_m indicates that a is 0.58. From Figure 2 we find that R_2/X must be 0.30, and hence R_2 is 0.11. This is the value required. A further check on slip by equation 30 indicates that it is 4.96 per cent.

REQUIRED CAGE DIMENSIONS

Common formulas for rotor resistance differ only in small details. A useful form is

$$\frac{2.4L \times C^2 \times k_w^2 \times 10^{-6}}{S_c \times S_2} + \frac{1.52D_r C^2 k_w^2 10^{-6}}{S_r \times P^2} = R_2 \qquad (31)$$

The first term applies to the bars, the second to the end rings. These are set up for the conductivity of copper and should be multiplied by the appropriate resistivity ratio, if other materials are used.

Because the laminations are already chosen, bar area and length are fixed, with only the resistivity left as a possible variable in some cases. It is therefore necessary to obtain the required resistance by adjustment of end-ring cross section, S_r. Let us call the first term R_b; hence

$$S_r = \frac{1.52\, D_r C^2 k_w^2 10^{-6}}{P^2(R_2 - R_b)} \qquad (32)$$

It is necessary to assume the probable end-ring shape or its radial thickness in order that the mean diameter of the end ring, D_r, can be chosen for this solution. Once this is done, all other terms are known, so that equation 32 can be solved directly for the required square inches of area.

Example Completed

The 25-horsepower motor, used as an example, required a rotor resistance of 0.11 ohm in stator terms, to meet the specifications. Slot areas are such that round copper bars having a cross-section area of 0.0655 square inch are used. Hence

$$R_b = \frac{2.4 \times 4.5 \times 120^2 \times 0.856 \times 10^{-6}}{0.0655 \times 60} \text{ or } 0.0338$$

For an assumed average ring diameter of 8.8 inches, each end-ring section is then

$$S_r = \frac{1.52 \times 8.8 \times 120^2 \times 0.856 \times 10^{-6}}{4^2(0.11 - 0.0338)} \text{ or } 0.1348$$

It is customary to check the copper loss in the ring, with the motor stalled, obtaining a value of loss per pound of end-ring material. To keep this to a reasonable value, it is frequently necessary to use higher resistivity materials. In this case the use of 40 per cent conductivity alloy would increase the section to 0.1348/0.40 or 0.336 square inches.

Summary and Conclusions

In the critical examination made of the D^2L analysis as a basis for polyphase-motor design there are several factors making this process inadequate as a design procedure. It is based on supplying "average" or "reasonable" values which are subject to such variation as to lead to indefinite results. It exercises no direct control over any of the defining factors for the motor rating, except through the electric loading as a measure of temperature rise. Of all of the characteristics on which to base motor proportions, this is the least satisfactory, as temperature rise is so thoroughly dependent upon ventilation and the mechanical design. Finally, no indication of the operating characteristics can be determined until the design work is completed and the winding constants calculated.

In contrast to that process, a design procedure has been set up in this paper, (based on a magnetic circuit of fixed contour) whereby the winding is fixed by direct solution. These equations involve the desired characteristics of the motor and physical constants of the laminations. The simplest approach has been found to be to fix the stator winding by the maximum torque, leaving the end-ring section of the rotor to supply all other performance items. This is a direct process, indicating almost at once whether the performance items can be met. Adjustment of the final design values are usually necessary only through the unfortunately gross steps of the standard wire sizes.

Such a process fits the usual design office requirements inasmuch as the majority of designs are made in laminations already available. Two procedures have been shown; one based on a fixed stack with the disadvantage of no direct control over the resultant flux density, and the other based on the division of the motor into two fictitious parts consisting of a stack motor with no end coil, and an end motor with no stack. The resultant equations make it possible to solve directly for the stack required to obtain a desired maximum torque with a fixed flux density.

The first step in this process is not original with the present writer, having been set up by the late W. J. Branson a number of years ago and used successfully in several design offices. The point of view from which his work was done and the continuation of his work into other parts of the design procedure have not been widespread, if one can judge from the inadequacy of the literature in this field.

On the matter of accuracy, no approximations have been made in any of the derivations except that the torque and current equations are based on the approximate equivalent circuit. The slip calculation does involve a slight approximation, but practice bears out its general accuracy. Bear in mind that it has been the intention in this paper to present a design point of view rather than a highly refined process. As adjuncts to this procedure, it is possible to make corrections in line with the more exact circuit theory, to consider skin effect and temperature change at starting, as well as current distribution in the rotor end rings. Neglect of these would constitute the greatest sources of error as indicated by practice.

Throughout this development the stator and rotor laminations have apparently been neglected although, obviously, their dimensions are of vital importance to the performance. A study of hundreds of laminations makes it possible to formulate some rules for lamination proportions and design. As could be expected from the relation of lamination constants, maximum torque, and densities, laminations of all sizes have an inherent maximum torque. Such data are of importance in attempting to build various powers and speeds in a given frame.

List of Symbols

A—area of one stator slot in circular mils.
a—ratio of starting to maximum torques.
b—ratio of starting volt-amperes to product of maximum torque and speed.
B_g—gap density in lines per square inch.
B_t—maximum tooth density in kilolines per square inch.
C—series conductors per phase.
C_s—series conductors per phase per inch of stack.
D—rotor diameter in inches.
D_r—mean diameter of the end rings in inches.
E—applied volts per phase.
k—fraction of total slot area available for copper.
k_w—winding factor.
l_c—length of conductor in inches.
L—length of stack in inches.
n—number of phases.
P—number of poles.
$\mathcal{P}$—permeance of all leakage paths, stator and rotor.
s—slip as a decimal fraction.
t_w—width of tooth in inches.
T_m—maximum torque in pound-feet.
T_s—starting torque in pound-feet.
T_{me}—maximum torque of "end" motor in pound-feet.
T_{ms}—maximum torque of "stack" motor in pound-feet.
X—total leakage reactance.
X_s—slot and zigzag leakage reactance.
X_e—end turn leakage reactance.
α—ratio R_1/X.
α_2—ratio R_2/X.
β—see equation 4.
$\mathcal{P}_s'$—permeance of slot and zigzag leakage paths, or any components, which vary with the stack.
$\mathcal{P}_e$—permeance of the end leakage flux paths.
R_1—resistance of the stator winding per phase.
R_2—resistance of the rotor cage in terms of stator winding.
R_b—resistance of the rotor bars in stator winding terms.
S_1—number of stator slots.
S_2—number of rotor slots.
S_c—cross-section area each rotor bar in square inches.
S_r—cross-section area each end ring in square inches.

Electronically Controlled Dry-Disk Rectifier

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IN spite of the practically universal use of alternating current for power generation and distribution, there exists an ever-increasing demand for direct current. To obtain direct current from the available supplies of alternating current, means of rectifying the alternating current have been developed. While many types of equipment are available, three types, each supreme in its own range, are found to be in general use for heavy-duty d-c power applications. The mercury arc has proved most efficient for the higher voltages from 250 volts on up. The intermediate range from 60 volts to 250 volts is supplied by the motor generator set. In comparatively recent years the low-voltage range from 0 to 60 volts has been taken over more and more by dry-plate rectifiers.

The dry-plate rectifier was first introduced approximately 30 years ago when the copper-sulfide rectifier was developed. Later in the early 1920's the copper-oxide rectifier was brought out. Recently, in fact during the last four years, selenium-oxide rectifiers have found wide usage in American industry. The selenium rectifier was developed and introduced in Europe in 1928. Continental acceptance of this rectifier has been widespread, for latest estimates indicate that 88 per cent of the metallic-junction rectifiers in use abroad are selenium.

Rectifier Characteristics

The selenium-oxide rectifier has much to offer in the way of stability, efficiency, economy, dependability, compactness, light weight, and ability to take heavy overloads for periods of short duration. Within the lower-voltage range it compares more than favorably with rotating equipment.

Similar to most electrical equipment, selenium rectifiers are designed for a temperature rise in continuous operation of 40 degrees centigrade above an ambient of 35 degrees centigrade. This means that up to the ultimate temperature there is no theoretical limit upon current handling capacity, and, consequently, the rectifier has the ability to handle large overloads for a short interval. The principle components of the selenium rectifier are transformers, selenium stacks, and bussing. These are all static elements, each of which has an indeterminately long life. Because of this freedom from moving parts, bearings, and commutators, the dry-plate rectifier offers completely dependable maintenance-free operation. Selenium stacks readily lend themselves to compact space design and even the largest rectifiers require no foundation.

The low forward resistance and high reverse resistance of the selenium stack combine to give the rectifier excellent economy and efficiency. Power tests show rectifier efficiencies of between 65 per cent and 85 per cent, depending upon the circuit and the load. The forward resistance of the stack is an inverse function of the stack current. Consequently, the I and R loss will tend to remain constant, and the rectifier will maintain high efficiency with unusual consistency from 20 per cent to 150 per cent of full load. In the larger equipment the efficiencies of rotating equipment compare with that of the selenium rectifier at full load, but the efficiency of rotating machinery drops rapidly as the load deviates from full load.

It is only in the matter of control and regulation of the output voltage that the average large selenium rectifier is surpassed by rotating equipment. Normal no-load to full-load voltage regulation for selenium rectifiers is 10 per cent to 18 per cent. While such regulation is satisfactory for most applications, there is still a great number of installations which require closer voltage control and regulation over a wide range. It was to meet this need that the fully automatic electronic control discussed in this article has been developed. The control makes it possible to obtain output voltages constant to one half of one per cent from selenium rectifiers even when the rectifier is subjected to widely varying loads and large changes in input line voltage. The design of the control is such that it can be used on either large or small rectifiers without any circuit changes.

Regulated Rectifier Basic Elements

The basic elements of a regulated rectifier (Figure 1) are a critically designed filter, a source of constant d-c potential, a high-gain d-c amplifier, thyratron-controlled saturating reactors and the selenium-oxide-rectifier disks with their associated transformers. In brief, the operation of these sections is as follows: The d-c voltage output of the selenium rectifier is passed through a filter to eliminate the a-c ripple and then compared against a constant d-c potential. This difference between the filtered rectifier voltage and a constant potential is applied to the grid of a high transconductance tube used as a d-c amplifier. Such circuiting gives relatively large d-c voltage changes from the amplifier for small changes in rectifier voltage. These changes in amplified direct current are used to form the basis for a d-c phase-shifting network that controls the grids of a pair of thyratrons. The regulated output of the thyratrons is circulated through the secondary windings (connected in series with proper phase relationship so that reflected primary voltages cancel)

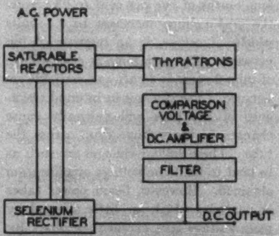

Figure 1. Block diagram

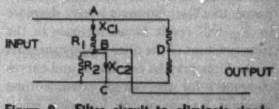

Figure 2. Filter circuit to eliminate ripple from d-c output voltage before comparing against fixed voltage

f = fundamental ripple frequency

$$X_{C1} = \frac{1}{2\pi FC_1}$$

$$X_{C2} = \frac{1}{2\pi FC_2}$$

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of saturating reactors that are in turn in series with the primary windings of the rectifier power transformers. The components of the control are sequenced so that if, for example, an increase in load were to tend to lower the rectifier voltage, the decrease in voltage would be amplified and used to shift the thyratron grids forward to cause greater saturating current to flow through the reactors decreasing the drop across the reactors and increasing the voltage across the power transformer.

Filter Design

One of the greatest single problems encountered in the development of the successful circuit was that of a suitable filter for the potential taken from the rectifier output to actuate the control. Because of the necessity for using a comparison voltage and an amplifier, even the small a-c ripple present in the output of a three-phase selenium rectifier is amplified enough, if applied directly without filtering, to render the phase-shifting circuit of the thyratron completely inoperative. The commonly used reactor-capacitor filters were found to be impractical. Their extremely high time constants make it impossible for such filters to follow rectifier voltage variations very closely with the result that they cause the rectifier to hunt and oscillate. The filter required therefore was one with a very high a-c attenuation factor, practically no d-c attenuation, and an extremely low time constant. The answer was found to lie in a variation of the Wien bridge (Figure 2).

By proper selection of constants it is possible to have the a-c voltage from B to C equal in phase and magnitude to the a-c voltage from D to C. As these vectors then subtract, the a-c potential from B to D equals zero. Resistance R_3 across X_{C2} acts to keep the d-c potential between B and C equal to zero. To hold the d-c attenuation at a minimum, X_{C1} should be very small as compared to X_{C2}; however, any decrease in X_{C1} will result in an increase in the time constant of the filter. The constants of the filter may be calculated and curves computed for the lowest possible time constant by use of the following equations:

$$Z_{BC}=\frac{R_3X_{C2}^2}{R_3^2+X_{C2}^2}-j\frac{X_{C2}R_3^2}{R_3^2+X_{C2}^2}=R_{BC}-jX_{BC}$$

As the voltage across BC must be in phase with the voltage across AB,

$$R_1/R_{BC}=X_{C1}/X_{BC}$$

The time constant of the filter is a function of C_1 and the resistance in series with it, that is, R_1+R_2:

$$\text{Time constant} \propto C_1(R_1+R_2)$$

For example, if in a three-phase full-wave rectifier whose fundamental ripple frequency is 360 cycles, $R_1=100$ ohms, X_{C1} equals 100 ohms (4.4 microfarads), and R_3 equals 2,000 ohms, the d-c output of the filter will be approximately 91 per cent of its input. If the time constant were to be cut in half by changing X_{C1} to 200 ohms (2.2 microfarad), the output of the filter would still be approximately 83 per cent of input.

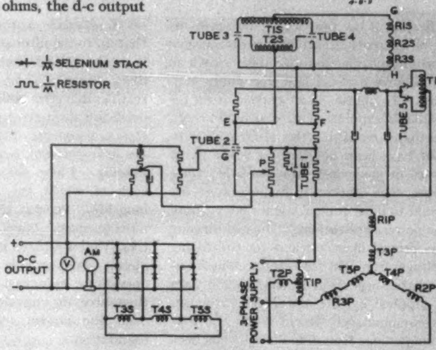

Figure 3. Simplified control circuit

T1P—Transformer 1 primary winding
T1S—Transformer 1 secondary winding
R1P—Saturable reactor 1 primary winding
R1S—Saturable reactor 1 secondary winding

Comparison Voltage and Amplifier

The output voltage of the filter is compared with a constant voltage obtained from potentiometer P (Figure 3), and the difference is applied to the grid of tube 2 for amplification. As we are taking the difference between two voltages, small changes in filter voltage mean relatively large percentage changes in the tube grid potential. For example, if the filtered voltage were 25 volts and the constant comparison voltage 24 the difference or applied grid voltage would be one volt. Now if the filtered voltage increases four tenths of one per cent to 25.1 volts the grid voltage increases to 1.1 volts which, of course, is an increase of ten per cent. The variations in grid voltage of tube 2 cause it to act as a variable resistance. The changes in internal resistance of the tube will naturally cause changes in the voltage drop across the tube. These voltage changes are such as to tend to limit the voltage amplification obtained. However, beam power tubes or pentodes which are little affected by plate voltage give excellent results. By operating tube 2 with a low screen grid potential which is maintained constant and proper choice of load resistance it is possible to obtain d-c voltage amplification of 20 to 30 decibels with one tube.

The constant voltage used for comparison against the filtered potential can be obtained by several methods. Probably the simplest source can be found in gaseous regulator tubes. Such tubes are readily available and are produced today with excellent characteristics and long life. The regulator tube shown as tube 1 in Figure 3 supplies constant voltage for two purposes. As the screen current of tube 2 will tend to vary with changes in grid voltages, additional amplification and stability is obtained by using the constant drop across tube 1 for the screen grid bias of tube 2. A potentiometer P bridged across the regulator tube provides the constant voltage to be compared against the filtered output. The potentiometer is the means by which the control is set for any desired rectifier output potential.

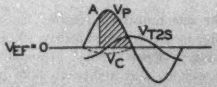

Figure 4A. Phase relations of thyratrons with d-c component of grid voltage equal to zero

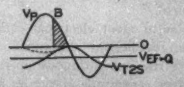

Figure 4B. Phase relations of thyratrons with d-c component of grid voltage equal to negative quantity

V_P—Thyratron anode voltage
V_{T2S}—A-c component of thyratron grid voltage
V_{EF}—D-c component of thyratron grid voltage
VC—Critical thyratron firing voltage

Phase-Shifting Network

As the internal resistance of tube 2 can be varied from extremely high values to very low ones, the potential across *EF* (V_{EF}) can be swung from positive through zero to negative. V_{EF} combined with the a-c output of transformer T_2 forms a d-c phase-shifting network that controls the grids and consequently the output of the thyratron, tubes 3 and 4. The voltage across the secondary winding of transformer 2 has been shifted 90 degrees with respect to the secondary winding of the plate transformer of the thyratrons, transformer 1. If V_P is the plate voltage of one of the thyratrons, and V_{T2S} is the grid voltage, Figure 4A shows that with $V_{EF}=0$ the grid-voltage curve intersects the critical grid curve at the point *A* where the tube fires and conducts current until $V_P=0$. However, if V_{EF} is equal to a negative quantity, Figure 4B, the tube will not fire until point *B* and will conduct correspondingly less than when $V_{EF}=0$. When V_{EF} goes positive, the critical grid voltage is intersected sooner, and more current is passed. The 90-degree phase shift required for V_{T2S} may be obtained by two methods. For single-phase operation (Figure 5) the primary winding of transformer 2 is connected in parallel with a resistance and both the transformers and the resistance are in turn connected in series with a capacitor. By making the impedance of the capacitor considerably more than that of the resistor, the current through the circuit can be made leading by almost 90 degrees. If this holds, then the voltage drop across the resistor, and consequently *T2P* will be shifted 90 degrees also. For three-phase operation the voltage shift between *T*1 and *T*2 is conveniently obtained by using the Scott connection for converting three-phase power to two-phase (Figure 6).

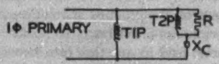

Figure 5. Single-phase network to obtain voltages shifted 90 degrees with respect to reference voltage

X_C is much greater than *R*

Saturating Circuit

Tube 3, tube 4, and transformer 1 form a controlled rectifier, the output of which is used to saturate the reactors *R*1, *R*2, and *R*3. The primary windings of these reactors are in series with the primary windings of the power transformers *T*3, *T*4, and *T*5. The secondary windings of the reactors are connected so that the a-c voltages induced from their primary windings are subtractive, and the a-c potential from *G* to *H* equals zero. The division of voltage across the primary windings of the reactors and transformers is a function of the reactance of the reactor, the secondary loading of the reactor, the reactance of the transformer, and the secondary loading of the transformer. The reactances of the elements are determined by their physical characteristics, namely, the number of primary turns and lengths, area, and composition of the magnetic path. The secondary loading of the transformer consists of the rectifier stacks and the rectified power load. The control of the entire unit is obtained from the secondary loading of the reactors, which is, of course, the d-c saturating current. Amazingly large amounts of power may be controlled over wide voltage ranges by comparatively small amounts of saturating power. For example, 1,500 watts of rectified direct current is being handled in a small installation by 50 watts of saturating power.

From the designer's standpoint the most difficult problem is that of determining reactor sizes to meet voltage specifications under no-load conditions. This is particularly true in cases where there are large fluctuations in line voltage or where wide-range voltage control is desired. Using the simplest type of series circuit shown in Figure 3, the no-load output voltage may be controlled from one-half to full voltage when the reactance of the reactor is equal to the reactance of the power transformer. Economically, it is not practical to try to obtain more than 50 per cent voltage control with the circuit shown. However, by proper circuiting it is possible to obtain zero to full voltage control without having to resort to the use of tap switches.

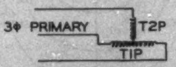

Figure 6. Three-phase network to obtain voltages shifted 90 degrees with respect to reference voltage

Performance

Although capable of extremely close control, the circuit as shown in Figure 3 is quite simple and uses relatively few vacuum tubes. Tubes were chosen with large cathode areas and are operated well below their rated output resulting in extremely long service life. Tube 5 is a rectifier tube supplying direct current for the amplifier tube 2. In controls for most industrial installations this tube would be replaced by a small selenium-oxide high-voltage bridge rectifier.

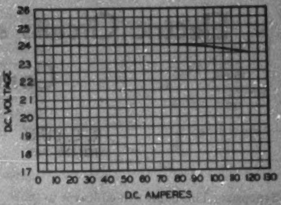

Figure 7. Performance curve of 100-ampere 24-volt electronically controlled selenium rectifier

Figure 7 is a graph showing the voltage regulation underload of a controlled selenium rectifier which is being used for aircraft "transceiver" testing. The specifications call for a 100-ampere rectifier capable of having its output voltage varied smoothly from 20 volts to 30 volts and having very fine no-load to full-load voltage regulation at a normal operating point of 24 volts. The curve given was obtained with a primary power supply that had a five per cent to ten per cent voltage drop at full load. While a slightly drooping voltage characteristic is usually desired, the circuit may be changed so that the rectifier exhibits either a constantly rising voltage or else a voltage characteristic similar to that of a compound generator. One very satisfactory method of obtaining an output voltage that is a function of the output current is that of inserting a current transformer in the a-c primary power supply of the rectifier. The output of the current transformer is then rectified, filtered, and fed with proper polarity into the control circuit between the Wien bridge filter output and the comparison voltage.

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Electric Circuits and the Magnetic Compass

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Synopsis: This paper will endeavor to show the deviations in the airplane magnetic compass caused by electromagnetic effects from the ship's wiring and will suggest some means that can be used to correct them.

GLOBAL warfare has extended the field of operations of aircraft beyond radio beams, landmarks, highways, and beacon lights, and, hence, has made the magnetic compass of paramount importance. Because of steel structures, armor plate, and single-wire electric circuits which carry increasingly larger currents, the magnetic disturbances at the conventional compass location have increased to such an extent that several remote indicating types of compasses have had to be developed.

The marked trend in the use of remote indicating types of compasses has created a shortage in these instruments. One of the purposes of this paper is to point out some of the problems created in the design of the air frame that can be solved by certain design techniques and the application of basic electromagnetic principles, thereby restoring the usefulness of direct-reading compasses and improving the accuracy of the remote indicating types. The scope of this paper will be limited to electromagnetic disturbances since the subject of ferromagnetism involving soft-iron effects, residual magnetism and its control in design and manufacturing processes, and other allied problems is too far-reaching to be treated other than in a separate paper. It can be noted that highly satisfactory results have been attained at Lockheed-Vega in the control of the ferromagnetic properties of air frames, both through design and demagnetizing processes during manufacture.

Aircraft electric systems conventionally utilize d-c generators, motors, and storage batteries. Certain limited applications of alternating current are found wherein 400-cycle inverters are utilized to supply small quantities of power for autosyn and similar transmitters, as well as for certain radio applications. However, the d-c supply and field winding of these inverters often cause noticeable magnetic disturbances. Furthermore, as a weight-saving measure, the air frame has been used as a ground return, leaving the electromagnetic field surrounding the single "hot wire" with little, if any, neutralizing field from a parallel ground-return conductor. Until the air frame is fitted with sources of alternating current to the exclusion of direct current, this problem must be faced by the electrical designer in his work on air frame electric systems and equipment.

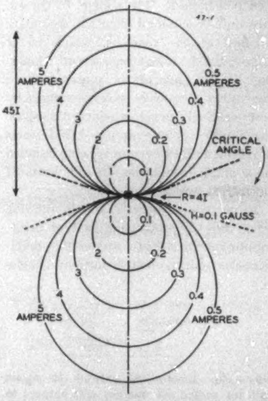

Figure 1. Horizontal wire-placement pattern for $H=0.1$ gauss. $D=1$ degree

The magnetic field in which the compass functions is extremely weak in comparison with the electromagnetic fields involved in power applications. The directive force utilized by the compass is the horizontal component of the earth's field, and this varies greatly over the surface of the earth, being zero at the magnetic poles and increasing to a maximum of approximately 0.40 gauss at the magnetic equator. When it is pointed out that the horizontal component of the earth's field along the North Atlantic ferry route varies between 0.15 and 0.10 gauss, it becomes apparent that a rather weak field surrounding a single-wire conductor in an air frame can cause deviations of a serious nature. Even in modern combat aircraft the remote indicating transmitter, located as favorably as possible, is subject to serious errors from this source. Since space limitations in aircraft dictate compass locations, and since the electric conductors must be located within the fuselage and wing surfaces, some serious problems arise that must be solved by ingenious electrical design.

The accepted practice in treating magnetic-compass problems is to resolve the magnetic field in question into two components: the horizontal component, which produces a directive force on the compass, and the vertical component, which has no directive force under normal conditions. Since the directive force is confined to the horizontal component of the magnetic field at the compass location, it will be affected only by the horizontal component of the magnetic field produced by any electric circuit. Any electric circuit may be separated into finite sections, and, if well-known electromagnetic formulas are used, the field at the compass location can be calculated for each section. These fields combined vectorially will give the components of the electromagnetic field caused by the electric circuit. Of course, it is the resultant horizontal component which directs the compass, and this will vary, not only with the intensity of the earth's field and the current flowing in

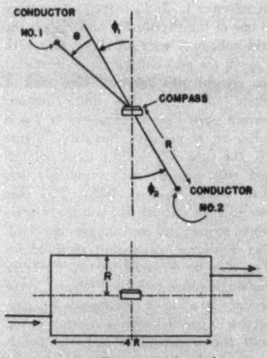

Figure 2. An illustration of angular asymmetry in split-wire neutralization

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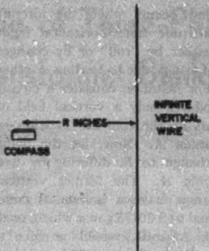
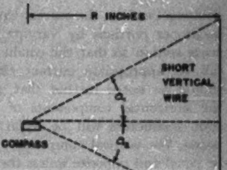
each electric circuit, but also will change with each heading of the aircraft.

This deviation cannot be corrected by means of the regular compass compensator, and to allow for it in navigation would require a book full of deviation cards; consequently, these effects must be reduced to a certain allowable maximum. For example, in Southern California, where the horizontal component of the earth's field is 0.26 gauss, a certain current causes a deviation equal to plus two degrees, whereas the same current causes plus four degrees deviation in more northerly magnetic latitudes, and if the current increases the deviation will increase still further.

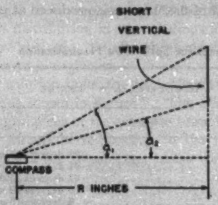
Present Air Force standards, as to deviation tolerances, are based on a horizontal component of the earth's field of 0.18 gauss, and a vertical component of 0.54 gauss, but, with aircraft operation increasing in northerly latitudes and transpolar air transportation a near actuality, the electrical designer can well anticipate a narrowing of the tolerance limits for compass deviations. Table I sets forth the calculated distances of a long straight horizontal conductor in a vertical plane above or below a compass location at which a deviation of one degree will be created by the currents designated for the three values of the earth's horizontal components shown.



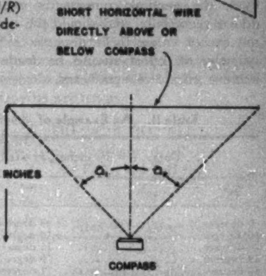
(a). Maximum deviation when wire is north or south of compass. Correct to ten per cent when D is less than 30 degrees. $D = 45\,I/R$ degrees



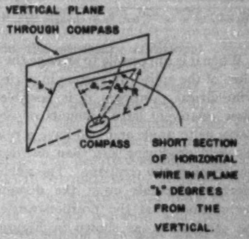
(b). No vertical component. Maximum $D = 22.5\ (I/R)\ (\sin a_1 + \sin a_2)$ degrees



(c). No vertical component. Maximum $D = 22.5\ (I/R)\ (\sin a_1 - \sin a_2)$ degrees



(d). Maximum when wire is north and south. No vertical component. $D = 22.5\ (I/R)\ (\sin a_1 + \sin a_2)$ degrees


VERTICAL PLANE THROUGH COMPASS
b
a_1
a_2
R
COMPASS
SHORT SECTION OF HORIZONTAL WIRE IN A PLANE "b" DEGREES FROM THE VERTICAL.

(e). Maximum when wire is north and south. $D = 22.5\ (I/R)\ (\sin a_1 + \sin a_2) \cos b$ degrees. Vertical field: $V = (I/25R)\ (\sin a_1 + \sin a_2) \times \sin b$ gauss

Figure 3. Compass deviations caused by straight conductors. $H = 0.1$ gauss

Table I. Calculated Distances, *R*, of Compass From Long Straight Conductor to Give Compass Deviation, *D*, of One Degree. Earth's Horizontal Magnetic Field in Gauss = *H*

Current		Distance for Deflection of One Degree					
		H = 0.1		H = 0.180		H = 0.264	
Ampere	Abampere	Centimeters	Inches	Centimeters	Inches	Centimeters	Inches
0.1	0.01	11.4	4.5	6.34	2.5	4.32	1.7
0.2	0.02	22.8	9.0	12.68	5.0	8.64	3.4
0.3	0.03	34.2	13.5	19.0	7.5	12.9	5.1
0.4	0.04	45.6	18.0	25.4	10.0	17.3	6.8
0.5	0.05	57.0	22.5	31.7	12.5	21.6	8.5
1.0	0.1	114	45.0	63.4	25.0	43.2	17.0
2.0	0.2	228	90.0	126.8	50.0	86.4	34.0
3.0	0.3	342	135.0	190.0	75.0	129.0	51.0
4.0	0.4	456	180.0	254.0	100.0	173.0	68.0
5.0	0.5	570	225.0	317.0	125.0	216.0	85.0
10.0	1.0	1,140	450	634	250	432	170
20.0	2.0	2,280	900	1,268	500	864	340
30.0	3.0	3,420	1,350	1,900	750	1,290	510
40.0	4.0	4,560	1,800	2,540	1,000	1,730	680
50.0	5.0	5,700	2,250	3,170	1,250	2,160	850

The notable changes in these distances caused by weaker horizontal components of the earth's field and increasing currents are of serious importance. A useful family of curves, shown in Figure 1, can be plotted by drawing circles with their centers on a vertical line through the compass location, tangent to the compass location with diameters equal to these distances. If a long horizontal north-south conductor is placed directly above or below the compass at a distance equal in inches to 45 times the current in amperes, then the deviation will be plus or minus one degree when the horizontal component of the earth's field is 0.10 gauss. This conductor can be placed anywhere on the circle with the same horizontal component at the compass if the current and direction remain the same. Where these curves approach the compass location, positioning of the conductor becomes quite critical to slight vertical displacements, and allowance should be made for possible changes in the position of either the compass or the conductor.

Figure 1 suggests a neutralizing technique where the removal of the conductor to a safe distance or the installation of a return ground wire is not desirable. The technique employs a division of the circuit between two parallel conductors. Each one is placed on its circle, one above and one below the compass location. By extending the split circuit a distance of four times the diameter of the circle used, 90 per cent of the disturbance will be neutralized. The use of this method will be confined to sections of the air frame which will give the necessary length and symmetry for properly aligning the conductors, such as in the fuselage, boom, or wing. Where space does not permit equal distances in each direction from the compass location, the current can be split in proper portions by varying the wire cross section so that the conductors will fall on appropriate current circles. It should be noted further that, although the horizontal components of the two split conductors will be equal and opposite, the vertical components will not be equal and opposite unless the conductors are diametrically opposite. However, the vertical components can be added vectorially, and should the resultant exceed values to be discussed subsequently, an effort should be made to achieve complete symmetry.

Table II. An Example of Angular Tolerances for Split-Wire Neutralization

Data: $R=36$ inches, $H=0.100$ gauss, $I=10$ amperes, $D=1$ degree

ϕ_1*	Θ	
0 degrees	+24 degrees 25 minutes	−24 degrees 25 minutes
10 degrees	+16 degrees 27 minutes	−24 degrees 25 minutes
20 degrees	11 degrees 46 minutes	−24 degrees 25 minutes
30 degrees	9 degrees 3 minutes	−18 degrees 40 minutes
40 degrees	7 degrees 26 minutes	− 9 degrees 16 minutes
50 degrees	6 degrees 24 minutes	− 7 degrees 25 minutes
60 degrees	5 degrees 46 minutes	− 6 degrees 14 minutes
70 degrees	5 degrees 22 minutes	− 5 degrees 50 minutes
80 degrees	5 degrees 11 minutes	− 5 degrees 30 minutes
90 degrees	5 degrees 8 minutes	− 5 degrees 22 minutes

* ϕ_1 may be + or −, but, as ϕ_1 changes sign, Θ values must also change sign. See Figure 2.

When it is impossible to arrange the two wires of the split-wire system in perfectly symmetrical positions, it is possible to allow relatively large amounts of asymmetry as to angle and still have a resultant field producing less than one-degree deviation of the compass. The angle of tolerance depends upon the distances, the current, and the position angle of the conductors, as shown in Figure 2. The tabulated values in Table II are obtained from the formula $B_2-B_1=0.072$ (I/R) $[\cos\phi-\cos(\phi\pm\Theta)]$, where B_1 and B_2 are the horizontal fields at the compass of conductors 1 and 2. For example, Table II illustrates a 20-ampere conductor which has been split at a distance of $4R$ with each leg carrying ten amperes and R taken as 36 inches.

After the horizontal component has been considered, the vertical component should be examined. A current-carrying conductor can lie precisely in a horizontal plane passing through the compass, and no horizontal component will exist to cause deviation of the earth's field. However, a vertical component of considerable intensity can exist, which, if excessive, can cause the compass card to dip into the vertical field. More important, however, is the resultant horizontal component that occurs when the aircraft changes attitude from horizontal flight position, either by "roll" or by changes in angle of attack due to loading or other causes. For instance, consider a circuit which produces only a vertical field of 0.020 gauss at the compass location in flight attitude A. Now, let the flight attitude change to B, differing by five degrees from A. The former vertical field will now have a horizontal component equal to 0.00175 gauss which, over the North Atlantic, would produce a deviation of one degree. This vertical field of 0.020 gauss is produced at a horizontal distance in inches equal to four times the current in amperes. That is to say, if 40 inches from a horizontal wire carries ten amperes, the field is 0.020 gauss. From the foregoing it would seem that a desirable compass-performance standard would be to limit the permissible deviation to one degree where the horizontal component of the earth's field is 0.10 gauss.

Figure 3 has been prepared to permit easy calculations where data are in inches, amperes, and degrees instead of centimeter-gram-second units. The formulas give the compass deviation D in degrees caused by straight conductors in various relationships to the compass location. All are based on a horizontal component of the earth's field of 0.10 gauss.

A complete compensation of any one circuit is possible by means of a compensating coil which carries the current equal or proportionate to the current in the disturbing circuit and is placed so that the magnetic field of this coil supplies a flux at the compass location which is exactly equal in magnitude and opposite in direction to the combined disturbance at that location of all the rest of the circuit.

The disturbance which will be caused by any single-wire circuit can be approximated from electromagnetic formulas by taking the circuit in sections, but it is far more satisfactory to measure the disturbance with a magnetometer in a finished airplane. The principle reason for the unsatisfactory solution arrived at by the mathematical treatment alone is that the return circuit through the air frame is not uniform, but is distorted by joints in the skin, extrusions attached to the frame, openings, and even magnetic fields. However, a direct measurement is relatively easy to make by use of a good magnetometer. The airplane is placed in flight attitude on a north magnetic heading, and the magnetometer pickup element is placed in the compass location so oriented that it will measure a horizontal component of the earth's field along the axis of the ship. The circuit in question is then switched on and off, and the magnetometer readings are taken for both field strengths. The difference gives the magnitude of this axial component of the disturbing electromagnetic field. The magnetometer pickup element is next oriented horizontally crosswise of the ship, and the same procedure is repeated. The third observation is made with the axis of the magnetometer vertical, and from these three components it is quite easy to determine the direction and magnitude of the disturbing flux. A small series coil may then be arranged in the neighborhood of the compass so that the field produced by this coil will, at the compass location, be equal and opposite to that produced by the circuit causing the disturbance. The coil then should be installed, and the result should be checked on all three axes with the magnetometer.

From the foregoing it is evident that the electric circuits necessary to the operation of the airplane are the cause of many compass errors. It is further seen that these errors can be anticipated by calculation from the geometry of the ship's wiring, and that the wiring can be rearranged so that the errors will be reduced to a negligible quantity, or, where it is not possible to move the wiring, it may be possible to find a solution by using double conductors, or coaxial conductors, or even by dividing the circuit into symmetrical paths. It is further shown that it is possible to compensate for the disturbance caused by any given electric circuit by the use of a series compensating coil, precalculated and properly oriented, to cancel out all components of the disturbing flux at the compass location.

Asymmetrical Loading of Three-Phase Three-Winding Transformer Banks

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Synopsis: It is accepted USSR practice to carry three-phase industrial load and single-phase traction load on a single three-phase three-winding transformer. For a given combination of loads it is necessary to determine the proper capacity relationships of the three windings.

This paper presents an analysis of the problem by the methods of symmetrical components. The data obtained from this analysis were used in setting up the standards for selection of transformer size for combined three-phase industrial and single-phase traction loading.

THE problem of unbalanced loading of three-phase transformer banks is of practical importance both in industrial installations where single-phase equipment, such as welders, is supplied and in railway installations where single-phase traction loads are fed from industrial three-phase banks. The present investigation deals only with the case of single-phase traction loading.

Since combination of symmetrical three-phase industrial load, and single-phase traction loading on three-phase transformer banks is no different from other types of asymmetrical loading, the solution to the one problem may be applied to all cases of asymmetrical loading practice.

The condition studied is that of the three-phase bank of three-winding single-phase transformers loaded asymmetrically. The transformer winding connections would be as follows:

1. The primary winding would be wye-connected with the neutral grounded.
2. The secondary winding would be delta-connected for symmetrical three-phase loading.
3. The tertiary winding would be wye-connected with the neutral ungrounded for asymmetrical loading.

The most frequent type of loading for traction circuits is the two-phase asymmetrical loading such as is shown in Figure 1.

In the solution of the problem of the capacity relationships of the separate windings, it is necessary to determine current distribution in the windings and effective voltage. The effect of magnetizing current is neglected.

The solution is for the equivalent star circuit as shown in Figure 2. The effective impedances of the separate branches of the circuit corresponding to the impedances of the transformer phase windings are

$$Z' = \frac{Z_{12}+Z_{13}-Z_{23}}{2} \qquad (1)$$

$$Z'' = \frac{Z_{23}+Z_{12}-Z_{13}}{2} \qquad (2)$$

$$Z''' = \frac{Z_{13}+Z_{23}-Z_{12}}{2} \qquad (3)$$

where Z', Z'', and Z''' represent the effective impedances of the branches of the equivalent circuit as shown in Figure 2, and Z_{12}, Z_{13}, and Z_{23}, represent the leakage impedances between windings as obtained from short-circuit tests.

The current distribution may be determined by the application of symmetrical components. In so doing, it is necessary to consider the internal impedances of the system. Refer again to the circuit of Figure 1, and if the neutral remains ungrounded, the asymmetrical loading has no zero-sequence current components, that is, $I_0=0$.

The current and voltage relationships for the circuit may be represented by the positive- and negative-sequence components

$$\dot{I}_a = \dot{I}_1 + \dot{I}_2 \qquad (4)$$

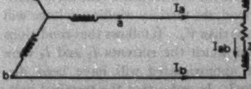

Figure 1. Method of connection of asymmetrical traction load to the star-connected secondary of a three-winding transformer

$$\dot{I}_b = a^2\dot{I}_1 + a\dot{I}_2 \qquad (5)$$

$$\dot{I}_c = a\dot{I}_1 + a^2\dot{I}_2 \qquad (6)$$

and

$$\dot{V}_a = \dot{V}_1 + \dot{V}_2 \qquad (7)$$

$$\dot{V}_b = a^2\dot{V}_1 + a\dot{V}_2 \qquad (8)$$

$$\dot{V}_c = a\dot{V}_1 + a^2\dot{V}_2 \qquad (9)$$

where $\dot{V}_1$ and $\dot{V}_2$ are the positive- and negative-sequence components of the voltage drop across the impedance of the external asymmetrical loads. Considering equations 4 through 9, equations for the circuits based on Kirchhoff's second law may be written as

$$I_a = \dot{I}_1 + \dot{I}_2 = \frac{\dot{V}_1(1-a^2)+\dot{V}_2(1-a)}{Z_{ab}} + \frac{\dot{V}_1(1-a)+\dot{V}_2(1-a^2)}{Z_{ac}} \qquad (10)$$

$$I_b = a^2\dot{I}_1 + a\dot{I}_2 = \frac{\dot{V}_1(a^2-1)+\dot{V}_2(a-1)}{Z_{ab}} \qquad (11)$$

$$I_c = a\dot{I}_1 + a^2\dot{I}_2 = \frac{\dot{V}_1(a-1)+\dot{V}_2(a^2-1)}{Z_{ac}} \qquad (12)$$

These equations, including the unknowns, $\dot{I}_1$, $\dot{I}_2$, $\dot{V}_1$, and $\dot{V}_2$, are not sufficient to complete the solution of the problem. To obtain the necessary additional conditions, it is necessary to determine the internal impedance components Z_0, Z_1, and Z_2 of the transformer windings.

Since it has been assumed that the applied primary potential is symmetrical, its zero- and negative-sequence components will be equal to zero. It follows that the effective electromotive force in the system of zero sequence and negative sequence will be zero, also, while in the positive-sequence system the electromotive force will be V_ϕ. These relationships may be expressed as

$$0 = \dot{I}_0 Z_0 + \dot{V}_0 \qquad (13)$$

$$\dot{V}_\phi = \dot{I}_1 Z_1 + \dot{V}_1 \qquad (14)$$

$$0 = \dot{I}_2 Z_2 + \dot{V}_2 \qquad (15)$$

to show the relationship of the effective electromotive forces of any system and the voltage drops in the internal and external impedances of the system.

Solution of the equations 10 to 15, inclusive, is in the form of

$$\dot{I}_1 = \frac{\dot{V}_\phi(Z_{ab}+Z_{ac}+3Z_2)}{Z_{ab}Z_{ac}+(Z_1+Z_2)(Z_{ab}+Z_{ac})+3Z_1Z_2} \qquad (16)$$

and

$$\dot{I}_2 = \frac{-\dot{V}_\phi\left(Z_{ab}a+\dfrac{Z_{ac}}{a}\right)}{Z_{ab}Z_{ac}+(Z_1+Z_2)(Z_{ab}+Z_{ac})+3Z_1Z_2} \qquad (17)$$

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where

$\dot{I}_1$ = positive-sequence current

$\dot{I}_2$ = negative-sequence current

Z_1 = positive-sequence impedance of the whole equivalent circuit

Z_2 = negative-sequence impedance of the whole equivalent circuit

Z_{ab} and Z_{ac} = impedance of the asymmetrical loads with reference to the primary winding as obtained from

$$Z_{ab}=\frac{V^2}{P_{ab}}(\cos\theta+j\sin\theta) \tag{18}$$

where

V = primary interphase voltage in kilovolts

P_{ab} = capacity of the corresponding branch of the circuit, in megavolt-amperes

$\cos\theta$ = power factor of the asymmetrical load

Table I. Load Distribution and Capacity Relationship for the Most-Loaded Phase of a Three-Winding Transformer With Asymmetrical Loading

Case Number	Asymmetrical Load Megavolt-Amperes		Symmetrical Load Megavolt-Amperes	Ratio of Asymmetrical Load to Total Load Per Cent	Winding Loads of the Transformer for the Most-Loaded Phase "a": Tertiary (Single-Phase Traction)		Winding Loads of the Transformer for the Most-Loaded Phase "a": Secondary (Three-Phase Regional) Megavolt-Amperes	Winding Loads of the Transformer for the Most-Loaded Phase "a": Estimated Primary Megavolt-Amperes	Winding Capacity Relationship According to Standard OCT 2,524 (Per Cent)		
	P_{ab}	P_{ac}			Megavolt-Amperes	Per Cent of Primary			Tertiary	Secondary	Primary
1	9.9	8.6	4.9	79	10.7	99	1.85	10.80	100	67	100
2	9.9	8.6	9.3	67	10.7	82	3.10	13.00	67	67	100
3	9.9	8.6	15.0	55	10.7	75	5.00	14.35	67	67	100
4	9.9	8.6	22.0	46	10.7	63	7.35	16.85	67	67	100
5	8.8	4.6	5.9	70	7.8	91	1.96	8.45	67	67	100
6	8.8	4.6	13.4	50	7.8	70	4.57	11.02	67	67	100

The internal, or applied negative-sequence impedance of the system includes also the impedance of the symmetrical load taken for the corresponding branch of the circuit

$$Z_p=\frac{V^2}{P_p}(\cos\beta+j\sin\beta) \tag{19}$$

where $\cos\beta$ is the power factor of the symmetrical load.

Since the positive- and negative-sequence impedances of the transformer are equal, Z_1 and Z_2, the impedances of the whole equivalent circuit will differ only by the difference between the positive and negative sequence impedances of the symmetrical load in the secondary delta winding.

To bring the total impedance in the positive- and negative-sequence system up to the point at which the asymmetrical load is to be applied, it is necessary to include the impedance of the parallel paths, both primary and secondary. This is expressed by

$$Z_1=\frac{Z'(Z_p+Z'')}{Z'+Z_p+Z''}+Z''' \tag{20}$$

referred to in Figure 3.

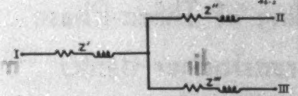

Figure 2. Equivalent star circuit of a three-winding transformer

By use of the principle of superposition, electromotive forces are assumed to be applied at this point, feeding the system by the positive- and negative-sequence currents. The effective negative-sequence voltage at the primary-winding terminals may be considered to be zero.

To establish the true current distribution in the transformer windings, it is necessary first to establish the distribution of currents of the positive $\dot{I}_1$, and the negative $\dot{I}_2$ sequences among the paths of the corresponding system. Such distribution is, of course, inversely proportional to the impedances of the parallel paths. Such current in each path must be added to the normal symmetrical current, I_p, of that path.

Then the current of the most-loaded phase of the primary winding will be

$$\dot{I}_a'=\dot{I}_p+\dot{I}_1+\dot{I}_2 \tag{21}$$

In approximating the current components I_1 and I_2 of the asymmetrical load, only the normal no-load phase voltage, V_ϕ, at the terminals of the star-connected winding is considered. The corresponding phase voltage at the primary-winding terminals will be $\dot{V}_\phi+\dot{I}_pZ'$ where $\dot{I}_pZ'$ is the primary-winding voltage drop caused by the symmetrical load on the delta-connected secondary.

After the application of the asymmetrical load, the tertiary-winding voltage will be less than V_ϕ. It follows that conditions under which the currents I_1 and I_2 were first approximated will have been disturbed. In order that the terminal voltage V_ϕ be maintained constant with load, it is necessary to increase the primary-winding voltage to $K(\dot{V}_\phi+\dot{I}_pZ')$ where K is a correction factor greater than one, based on the voltage drop caused by the asymmetrical load.

The voltage under asymmetrical-load conditions at the terminal of the tertiary winding, point III of the equivalent system of Figure 3, may be obtained by application of the methods of symmetrical components to the equivalent system. The positive-sequence voltage-drop component, starting from zero potential at point I in Figure 3, is $(-\dot{I}_1'Z'-\dot{I}_1Z''')$ where I' is the component of the positive-sequence current in the path Z'. Similarly, the negative-sequence voltage drop at the same point is $(-\dot{I}_2'Z'-\dot{I}_2Z''')$ where $\dot{I}_2'$ is the negative-sequence current in the path Z'.

It follows that, in Figure 3

$$\dot{V}_1=\dot{V}_\phi-\dot{I}_1'Z'-\dot{I}_1Z''' \tag{22}$$

and

$$\dot{V}_2=-\dot{I}_2'Z'-\dot{I}_2Z''' \tag{23}$$

where $\dot{V}_1$ and $\dot{V}_2$ are positive- and negative-sequence voltages, respectively, at point III.

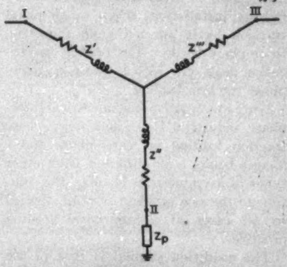

Figure 3. Equivalent star circuit of a three-winding transformer showing balanced load on the delta-connected secondary

The actual phase voltage will be

$$\dot{V}_a=\dot{V}_1+\dot{V}_2 \tag{24}$$

$$\dot{V}_b=a^2\dot{V}_1+a\dot{V}_2 \tag{25}$$

$$\dot{V}_c=a\dot{V}_1+a^2\dot{V}_2 \tag{26}$$

and since

$$a^2=-0.5-j0.866$$

and

$$a=-0.5+j0.866$$

equations 25 and 26 may be rewritten as

$$\dot{V}_b=-0.5(\dot{V}_1+\dot{V}_2)+j0.866(\dot{V}_1-\dot{V}_2) \tag{27}$$

$$\dot{V}_c=-0.5(\dot{V}_1+\dot{V}_2)-j0.866(\dot{V}_1-\dot{V}_2) \tag{28}$$

From these equations the interphase voltages may be calculated to be

$$\dot{V}_{ab}=\dot{V}_a-\dot{V}_b=1.5(\dot{V}_1+\dot{V}_2)+j0.866(\dot{V}_1-\dot{V}_2) \qquad (29)$$

$$\dot{V}_{ca}=\dot{V}_c-\dot{V}_a=-1.5(\dot{V}_1+\dot{V}_2)+j0.866(\dot{V}_1-\dot{V}_2) \qquad (30)$$

$$\dot{V}_{bc}=\dot{V}_b-\dot{V}_c=j1.732(\dot{V}_1-\dot{V}_2) \qquad (31)$$

Table II. Relationship of Phase Load to Phase-Winding Load

Asymmetric Load in Per Cent of Total Load	Phase-Winding Load in Per Cent of Phase Load
80	100
70	91
67	82
55	75
50	70
46	63

In the foregoing discussion a factor *K* was presented to correct for the difference between load and no-load conditions of difference between terminal voltage at point III in Figure 3 and primary voltage at point I. The value of *K* may now be obtained as the arithmetic mean value of the calculated voltage ratios for the two loaded phases. If

$$K'=\frac{\sqrt{3}V_\phi}{V_{ab}} \qquad (32)$$

and

$$K''=\frac{\sqrt{3}V_\phi}{V_{ca}} \qquad (33)$$

then

$$K=\frac{K'+K''}{2} \qquad (34)$$

and

$$\dot{V}_a'=K(\dot{V}_\phi+\dot{I}_p'Z') \qquad (35)$$

where $\dot{V}_a'$ is the primary voltage at point I in Figure 3.

Conclusion

The foregoing analysis of the loading of asymmetrical three-phase three-winding transformer banks has been verified by laboratory observations of current distribution and effective voltages under such conditions. A thorough practical investigation has confirmed fully the correctness of the conclusions arrived at mathematically.

It appears to be reasonable that equations established in this manner may be used in estimating the required capacity of the most-loaded phase of three-phase three-winding transformers subjected to such loading.

The results of calculations made for different combinations of symmetrical regional and asymmetrical traction loads are given in Table I.

In expressing the *A*-phase capacity in kilovolt-amperes, the transformer primary-winding power for this phase is represented by

$$P_a'=\dot{I}_a'V_a' \qquad (36)$$

The predetermined transformer loads are represented in Table I. As a first approximation, it is assumed that the phase load on the traction winding is the sum of the two path loads $P_{ab}/\sqrt{3}+P_{ca}/\sqrt{3}$ which fall on this phase, as shown in Figure 1. Actually this approximation will be higher than the measured winding load.

The symmetrical load of the delta-connected secondary is assumed to be the same in all three phases.

The results of the foregoing analysis disclose a characteristic feature of load distribution in three-phase three-winding transformers under the conditions of asymmetrical loading. As shown in Table I, the *A*-phase primary winding does not carry the sum of the secondary- and tertiary-winding loads of this phase as it would in the case of symmetrical loading. A part of the load of the most-loaded *A* phase will be carried by the other less-loaded phases.

The portion of the total phase load carried by the phase winding varies with the relationship between asymmetrical load and total load. The relationship for the most-loaded phase is shown in Table II.

According to Standard for Transformers of the Union of Soviet Socialist Republics, *OCT* 2,524, the size of the single-phase transformers of the three-phase bank will be determined by the relationships 100:67:100 and 100:67:67 corresponding to winding voltages of 110 kv, 35 kv, and 11 kv, respectively. These relationships are used for practical application to this type of loading.

Electrification of Large Open-Pit Copper Mines

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Synopsis: The transportation problem of a large strip mine involves the loading, hauling, and dumping of both ore and waste. These materials are loaded with large electric shovels operating on benches along the slope of a hill or pit. The ore is transported over main-line tracks to the reduction plant, while the waste is hauled to suitable dumps. All main-line tracks are permanent and are equipped with permanent overhead distribution systems (Figure 1). All bench and dump tracks are portable and are equipped with portable overhead distribution systems.

Sixty per cent of the world's copper is now obtained by open-pit mining of low-grade ore. Electrification of haulageways for open-pit mining is keeping pace with the expansion and is modernizing the transportation of huge tonnages. Huge capacities necessitating heavy cars and heavy grades demanding short-period overloads have been responsible for the selection of the electric locomotive for motive power. From the standpoint of locomotive capacity, rail size, dump-car capacity, and density of traffic, most open-pit electrifications compare in size with our large steam-road installations and hold an important place in the field of heavy traction (Figure 2).

In addition to describing the haulage problem of large open-pit copper mines, this paper discusses the design of permanent and portable distribution systems.

OPEN-pit mining, commonly called quarrying, is familiar to everybody and can scarcely be termed a new process, since materials for the many historic structures of Egypt, Rome, and China were obtained by "pit quarrying." However, open-pit mining, as applied to modern large tonnage operations, is a highly developed and highly mechanized process for recovering minerals from large ore bodies and is dependent on heavy haulage facilities, usually electrified.

Our domestic mine output of copper, estimated in excess of 1,000,000 tons for 1942 and representing 15 per cent increase over 1940, has been possible, chiefly because of the development of open-pit mining methods. Because of high costs, underground mining is greatly limited to high-grade ores and to comparatively low tonnage. Deeper shafts and greater penetration increase the underground haulage costs, since access to the ore body must be made from the surface, and the shafts cannot be moved at will. Then, too, the depletion of easily available deposits of high-grade ore has influenced the expansion of open-pit mining. Therefore, either the mining of low-grade ores or the removal of large deposits involving considerable overburden and waste rock is almost exclusively a stripping operation. Strip mining has not only given the operator an economical plant but has put an end to an important waste in his plant—the waste of natural resources.

The Utah Copper Company is credited with originating the recovery of copper from low-grade ore on a large tonnage basis. Other companies, including Nevada Consolidated, Ray Consolidated, and Chino (Nevada Consolidated in New Mexico), furthered the development and commercialization of the process. South America and Africa are also large producers of copper from low-grade ores, the Katanga project in the Belgian Congo representing the most recent undertaking. At the present time 60 per cent of the world's copper is secured from mammoth strip operations in low-grade copper.

It has been only a short time since the bulk of our domestic copper was mined in Montana and Michigan, both states operating immense underground workings. Today the open pit mines in Arizona, Nevada, New Mexico, and Utah are producing large quantities of copper from low-grade ores, and, as a consequence, Montana and Michigan are third and sixth, respectively, on the list of copper-producing states. Some detailed data for 1941 may be found in Table I.

Table I. Copper-Producing States

State	Tons
Arizona	326,000
Utah	267,000
Montana	128,000
Nevada	79,000
New Mexico	73,000
Michigan	46,000
Other states	38,000

It is interesting to note from Table I that Arizona and Utah produced 62 per cent of the domestic copper in 1941. Statistics also show that these states produced 18 per cent of the world copper in 1939, the last year for which accurate figures were available. Perhaps a description of one of the most recent open-pit undertakings will acquaint you with modern large-scale stripping operations, and also with the importance of adequate transportation facilities for handling the thousands of tons of ore and waste. This operation is the Morenci mine of the Phelps Dodge Corporation in Arizona.

Typical Example of Open-Pit Mining

Early mining operations at Morenci were underground. Evidence of this early mining can be seen in several locations in the new pit, where stripping has broken through the old workings. Only the high-grade deposits attracted the first miner. Operations were restricted to the secondary enriched areas and to replacement deposits occurring in sedimentary rocks. The copper content of these ores was adequate to warrant the cost of mining, transporting, milling, and smelting. Following the exhaustion of these ores, mining operations were transferred to the monzonite porphyry, where rich ore was again found in fissures also as disseminations in the porphyry. Later, mining was extended to the lower-grade disseminated ores where operation by the block-caving method continued for many years.

All of this early mining contributed to the development of the new Morenci pit. Commercial mining uncovered the large deposits of low-grade porphyry ore. Extensive exploration work with churn drills, diamond drills, and continued underground mining fixed the pit limits and determined the size and quality of the huge ore body.

The low-grade ore of the Morenci pit consists chiefly of chalcocite disseminated in monzonite porphyry. Chalcocite, a sulphide ore, occurs in many of the western open-pit copper mines, such as the Bingham mine of the Utah Copper Company, the Phelps Dodge Corporation Ajo pit, and the open pits of the Nevada Consolidated Copper Corporation at Santa Rita, N. Mex., and McGill, Nevada. Rarely does the copper content of these large open-pit mines average more than one per cent. At Morenci, the ore averages 1.06 per cent copper.

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Economy dictates the amount of waste that can be removed in any stripping area (Figure 3). The cost of loading, hauling, and dumping must be maintained within a definite figure. Too much waste removal, long hauls, and distant waste dumps increase mining costs. For all stripping operations the amount of waste that can be removed economically bears a definite relationship to the amount of ore recovered. For coal stripping, the miner's rule of thumb is one foot of overburden for one inch of coal. In low-grade high-tonnage copper-stripping operations the ratio is one ton of waste for one ton of ore. Therefore, the hauling of ore is one half of the problem, and the hauling of wastes is the other half.

At Morenci the waste includes an overburden, or capping, which has a maximum thickness of 500 feet and an average thickness of 216 feet above the ore body. Preliminary stripping required the removal of 50,000,000 tons of material, mostly waste, in order to assure the initial removal of 25,000 tons of ore daily. All material not used for truck highways was hauled to dumps or to the railroad right of way for fills. The preliminary digging in was accomplished with 18 large trucks capable of handling $23^1/_2$ yards of material. Many miles of excellent roadway were constructed for truck haulage, since the loaded truck weight was 65 tons, and grades were as high as ten per cent.

The removal of ore at Morenci is accomplished by the establishment of a series of benches, the number depending on production requirements (Figure 4). Since each bench represents a separate or independent mining operation, this bench method of open-pit mining is usually the most economical. The benches are equivalent to a stairway, each tread representing the floor where the shovels operate to load dump cars, each riser representing the working face where the ore is broken away. The height of the bench is determined by the shovel equipment, limitations to drilling, and the nature of the material. In this case the height of each bench is 50 feet, and its width, being determined by the requirements for shovel operation and haulage facilities, is 100 feet.

The Haulage Problem

Following the establishment of benches, the ore and waste rock are broken away from the face by blasting, the length of the blasted section often extending a distance of 1,000 feet (Figure 5). For loading purposes, secondary blasting is usually necessary for further fragmentation of the larger rocks. Additional blasting is also necessary for removing the toe where the face and floor intersect. Removal of the toe provides a vertical bank and reduces overshooting and undershooting, thus permitting the burden carried by each drill hole to be estimated more accurately. The preparation of ore, therefore, becomes an important factor in the haulage problem. The track and overhead distribution system must be planned either for quick removal or for resisting a certain amount of flying rock fragments.

The loading of ore and waste at Morenci is accomplished with a full-revolving electric shovel equipped with a $4^1/_2$-yard dipper (Figure 6). A bulldozer aids in leveling the bench and scraping stray rock towards the reach of the shovel. It also serves to move the spillage from the area of the track. The ore is loaded in trains of ten 40-ton side dump cars, each equipped with axleless trucks and roller bearings, while the waste is loaded in trains of nine 30-ton cars of equivalent design (Figure 7). Sufficient cars or trains are available at the shovels to maintain a shovel efficiency of 87 per cent. Clearance, then, is another important factor in the planning of the open-pit mining operation. There must be sufficient clearance and overhead to permit unrestricted operation of the shovel, and there must be sufficient track clearance to permit the unrestricted movement of the bulldozer (Figures 8 and 9).

Naturally, the tracks on the benches and waste dumps cannot be aligned and surfaced as well as main-line tracks. Settlement on the fills and uneven floor on the benches makes surfacing difficult. The constant moving of tracks on both the waste dumps and the benches prevents good track alignment. As a result locomotives and rolling stock are designed to meet this problem, and the locomotive, particularly, must successfully withstand short-time overloads when negotiating uneven track and severe grades.

As explained in this description of the Morenci operations, open-pit mining consists of the removal of ore and waste from a series of shelves or benches, one above the other, and each operates as a separate mine. The operation may be either a pit operation or the tearing down of a mountain. Benches are employed in either case. The actual mining consists of blasting down ore, loading in suitable dump cars, and hauling ore to the mill or reduction plant. The haulageway includes permanent tracks and portable tracks. The permanent tracks are located between the mine and mill and also include the storage yards. The portable tracks are located on the benches and on the waste dumps. Switchbacks that are necessary for ascending from one bench to the next are usually permanent tracks. The overhead trolley system must correspond to the track construction. It must be either permanent or portable.

Progress of Electrification at Large Western Mines

The four large open-pit copper mines located in the West and Southwest, that are either wholly or partially electrified, are moving a daily tonnage of approximately 300,000 tons of ore and waste. This material represents more than 6,000 carloads and is handled with 100 electric locomotives over 200 miles of track. The locomotives at the Utah Copper Company, and also the two Nevada Consolidated Copper Corporation's operations at McGill, Nev., and Santa Rita, N.Mex., are 85-ton machines, whereas the 16 locomotives at Morenci are 125-ton machines. All mines operate with an overhead trolley voltage of 750 volts direct current. The combined substation capacity of the four electrifications for traction purposes is 15,000 kw, and the combined locomotive capacity is 100,000 horsepower.

Overhead Distribution System

The overhead distribution system performs a very important part in the operation of the open-pit mine, and is not merely "a place to park the pantograph." The steam locomotive requires its coaling equipment, its water plant, and its cinder pits. The diesel-electric locomotive requires its fuel storage and its fuel distribution system. The electric locomotive requires its substations and its overhead distribution system. Therefore, the electric distribution system becomes the auxiliary equipment for the electric locomotive. The distribution system is the fueling system.

The specifications for the two types of overhead systems, permanent and portable, are quite different. The permanent track is operated with the usual type of pantograph mounted on the roof of the cab, while the portable track is operated with a side-arm collector. The trolley wire is over the track center on permanent right of way and offset to the

Figure 1. Over 6,000 cars of ore and waste are hauled daily by electric locomotives in the large open-pit copper mines of the United States

Figure 2. The electrification of open-pit mines has become an important factor in the field of heavy traction

Figure 3. During preliminary stripping at Morenci, waste material was used for roadways and railroad fills

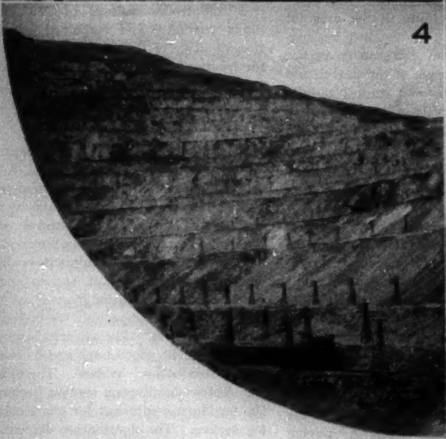


Figure 4. Each bench represents a separate or independent mining operation

Figure 5. Ore is broken away from benches by blasting, the length of the blasted section often extending a distance of 1,000 feet

Figure 6. Full-revolving electric shovels with $4^1/_2$-yard dippers loaded $22^1/_2$-yard dump trucks during preliminary digging in at Morenci

side of the track along the portable sections. We will describe the portable overhead first.

Portable Overhead System

The portable overhead system is used at any location where it is necessary to move the track at frequent intervals. The tracks on the benches are moved towards the face as mining progresses, and the tracks along the waste dump are moved towards the shoulder of the dump as the dump is filled out. On the bench tracks, head clearance must be maintained for loading, since the dipper of the shovel reaches over the center line of track. Crane equipment is frequently operated over both the bench tracks and the dump tracks; therefore, overhead clearance is necessary over these tracks. The usual offset of trolley wire from the center of track is from 10 to 16 feet. Of course, the offset on the benches is on the side opposite the shovel.

Portable structures are usually steel, either constructed of fabricated shapes or pipe. Owing to the constant handling, a wooden structure does not have sufficient life and is easily splintered. The pipe structure is the simplest and requires less maintenance.

One of the simplest pipe structures is used on the Utah operations at Bingham. This structure consists of a five-inch pipe embedded in a concrete footing and supporting, at its top, a bracket arm which in turn, supports the trolley wire. The concrete footing is elongated, as illustrated in Figure 10 and is equivalent to a sled or stoneboat in contour. It is equipped with a large eye bolt for pulling and can also be shoved into position with a bulldozer. This type of portable structure is easily transported from one bench to another and can be moved easily as mining progresses. A similar structure is used by the Canadian Johns-Manville Company for the mining of asbestos at their open-pit operation at Asbestos, Que., Can. This structure, however, is fabricated of lighter pipe. The illustration in Figure 11 shows how the footing is spread and mounted on stringers. This structure does not require a bracket arm for supporting the trolley wire; the gooseneck upper end of the main pipe performs this function. This type of structure has no flexibility from the standpoint of trolley-wire alignment. The entire structure must be moved along the ground to obtain this feature. Because of its lightness it is easy to move, but the curved arm does not have ample reach for some types of collectors.

One of the most recent and useful types of portable supporting structures is used at the new Katanga copper mine in the Belgian Congo. This structure, Figure 12, employs a five-inch tubular pole, a base, a bracket arm, and a special pole collar. The pole is supported in a cast-iron socket. The pole band has a large vertical hole or socket into which a pin on the bracket is inserted. A pad on the lower end of the knee brace lays against the pole for additional support. Since the center of rotation of the bracket arm about the pin does not coincide with the center of rotation of the pad, very little swing can be obtained by the bracket arm. However, there is sufficient clearance between the pin and the socket in the pole band to permit some movement, thus providing relief in the bracket arm in case the trolley-wire tension becomes unequal on either side of the pole.

To install the structure, the cast-iron socket is first placed in position. The socket may either be attached to a sled, in which case it is integral with the sled, or it may be spiked to extended cross ties. Next, the pole is inserted and keyed in the socket. This is usually a crane job with the locomotive crane pulling a flat car on which the structure material is loaded. The bracket arm is then lifted and its pin dropped into the socket in the pole band, the pole band having first been attached to the pole at the proper height before the pole is set in the base. The trolley wire is then strung and tensioned.

When the portable structures are hauled to new locations, the assembly procedure is sometimes altered. Another pole band is attached to the pole at a location about five feet above the ground. After the five-inch pipe has been dropped and keyed into the base, the pole bracket is then attached to this second pole band nearer the ground. At this height the lineman strings the trolley wire. After the wire is in place the brackets are lifted, one at a time, and placed on the pole bands at the top of the poles and at the proper height. Of course, the trolley wire is slack when the bracket arms are lifted; otherwise, the wire would tend to tip the poles. After the bracket arms are in place at the top of the pole, the trolley wire is tensioned and clamped.

A special type of trolley clamp is used for portable structures (Figure 13). This clamp provides not only the usual jaws for clamping the grooved trolley wire but also an opening above the jaws where the wire can be supported but not clamped. When the overhead system is in operation, the trolley wire is clamped in the lower part of the jaws and cannot be pulled through the jaws, but, while the structures are being moved, the trolley wire is held loosely in the opening above the clamps. Thus, the trolley wire is free to slide longitudinally when the poles are moved to a new position. After the structures are in place the trolley wire is tensioned by pulling the free end. The wire is then dropped into the lower portion of the clamps, and the jaws are tightened on the wire. The reverse of this operation is necessary when it is time to move the poles to another location. The jaws of the trolley-wire clamp are first loosened, and the trolley wire is pushed up and into the upper recess. The jaws are then tightened again in order to prevent the wire from falling out of the clamp.

The trolley clamp is usually insulated with a composition hanger with a malleable-iron protecting shell which withstands the flying rock fragments.

The fabricated structure illustrated in Figure 15 supports both the 750-volt d-c trolley wire and the 5,500-volt a-c electric-shovel circuit at both Utah Copper and the Santa Rita operation of the Nevada Consolidated Copper Corporation. In some instances the d-c feeder is also carried on the portable structures; however, the method of feeding the bench tracks does not require longitudinal feeder.

The installation of over 2,000 portable structures at the Bingham mine of the Utah Copper Company has demonstrated the usefulness of this type of haulage. Some of the original locomotives at Bingham were equipped with storage batteries to be used for the supply of energy in sections of waste dumps and other locations. It was thought that portable poles could not be used. Today all operations are supplied with an overhead trolley wire, batteries having disappeared from the scene. With adequate overhead clearance and properly designed structures there is no interference with loading and unloading, and the portable system can be moved quickly and cheaply. With a force consisting of a crane, three pit crews for the track, and two electricians, Utah moves 3,000 feet of track and portable overhead in five hours.

Permanent Overhead System

Two types of overhead are used on permanent track. Both types must provide clearances for pantograph operation.

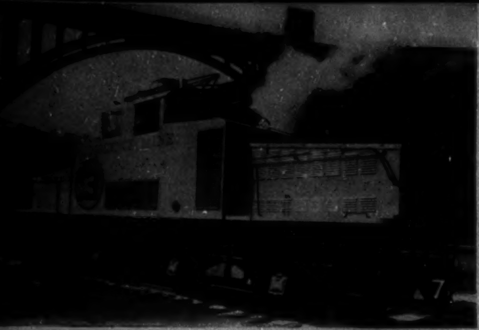


Figure 7. 125-ton 750-volt locomotive operating at Morenci

Direct suspension, usually requiring supplementary feeder, is selected for slow speeds and full load currents below 1,000 amperes. Catenary overhead is selected to reduce initial cost and the number of supporting structures and to provide a better means for the supplying and collection of high currents. The choice of the distribution system, therefore, is based on the limitations and specifications of each electrification. As examples, the recent Katanga installation in the Belgian Congo with 66-ton locomotives is constructed with direct suspension using a single 4/0 contact wire on permanent or main-line sections, whereas the Morenci mine of Phelps Dodge Corporation with 125-ton locomotives is constructed with a compound catenary system consisting of a 350,000-centimeter hard-drawn copper main messenger cable, a 3/0 19-wire hard-drawn copper intermediate messenger, and 4/0-grooved hard-drawn copper contact wire. As a general rule, yards are constructed with light catenary in order to reduce the number of supporting structures and provide greater visibility, as well as man clearance around cars. The Utah Copper Company and the Nevada Consolidated Copper Corporation have installed many miles of light catenary along permanent tracks including storage yards, crusher yards, and main haulage out of the mines. The light catenary consists of a 7/16 Siemens-Martin messenger cable supporting a 4/0-grooved contact wire with flexible hangers.

Simplified Catenary Span

With reference to the Morenci mine of Phelps Dodge Corporation, the type of haulage made possible the selection of a modified type of catenary which, heretofore, has not been applied to mine electrification. Ease of construction and maintenance are the chief advantages of this type of catenary, but it is designed also for good current collection and feeder characteristics. The catenary span is spaced 120 feet on tangent, and on curves it is spaced 80, 60, and 50 feet, depending on the degree of curvature. Most of the curves, however, use 80-foot spacing.

With reference to Figure 16, it may be observed that three main-line hangers

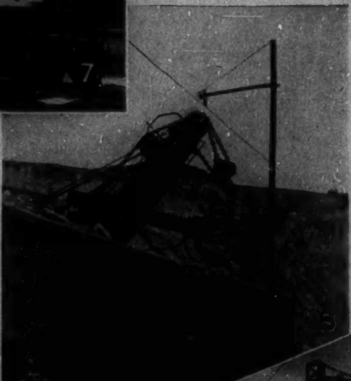


Figure 8. Unrestricted overhead clearance must be provided for the operation of the electric shovel

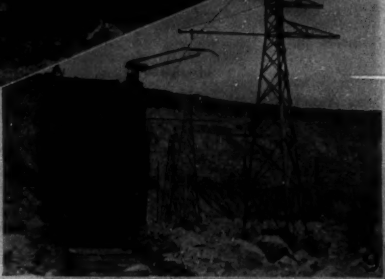


Figure 9. Side-arm collection provides overhead clearance for the movement of locomotive cranes

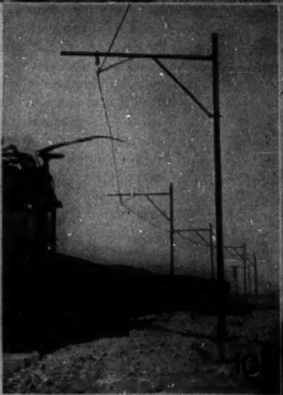


Figure 10. Portable structures consisting of tubular poles supported on concrete stoneboats are used on benches at Utah Copper Company

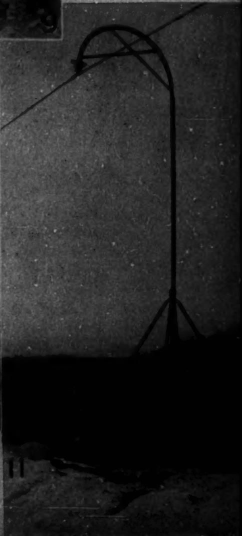


Figure 11. Canadian Johns-Manville Company, Ltd. portable pipe structure

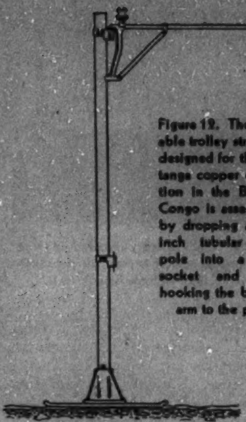


Figure 12. The portable trolley structure designed for the Katanga copper operation in the Belgian Congo is assembled by dropping a five-inch tubular steel pole into a base socket and then hooking the bracket arm to the pole

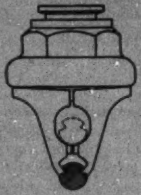


Figure 13. When a portable structure is to be moved, the grooved trolley wire is removed from the jaws of the clamp and inserted in an upper groove where the wire will slide freely

are used on the 120-foot spans. The end hangers are located 20 feet from each point of support, while the third hanger is located midway on the span. This main-messenger-hanger spacing divides the intermediate cable spans into 40-foot lengths. The trolley wire is supported by three clips attached at equal intervals to the intermediate cable. The center clip is two inches in length, whereas the two end clips are three inches in length. With this type of construction good contact between fittings and cable is obtained for the flow of electric energy from the messengers to the contact wire. As explained in the preceding description, a minimum number of hangers is required. The 120-foot span requires only two lengths of main-line hangers and two clips. The 80-, 60-, and 50-foot spans require additional lengths of main-line hangers, but the same two-clip construction is used throughout the entire line. With this type of catenary span the total number of main-messenger hangers will not exceed 9 for the spans indicated, whereas the total number of hangers for the conventional span might easily exceed 50 when the special length spans are considered.

Figure 14. Portable structures mounted on truck wheels are moved over short sections of rail on dump tracks of International Nickel Company

Figure 15. Fabricated structures support the 750-volt d-c trolley wire and the a-c shovel circuit at Utah Copper Company

Although tangent pole spacing on the Morenci electrification is only 120 feet, and the nominal pole spacing on light catenary at the mines of the Utah Copper Company and the two operations of the Nevada Consolidated Copper Corporation is 150 feet, the spacing of catenary spans is not limited to these dimensions, except by economy and physical limitations (Figure 17). These operations, from the standpoint of rolling stock, locomotives, and rail size (131 pounds at Morenci) are comparable to many large steam-road electrifications, and with sufficient main-line track would undoubtedly be built with structures spaced at 300-foot intervals.

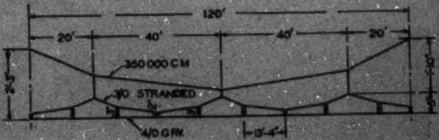
A Bit of History

The Utah Copper Company electrified the crusher yards at both the Magna and Arthur plants in 1925 (Figure 18). Two 80-ton locomotives, formerly used by the Manufacturers' Railroad of St. Louis, were installed in these yards. The total trackage was slightly over eight miles. During 1936 a large part of this crusher-yard electrification was torn down, and one of the mills was partially dismantled. During the following year, however, because of improved market conditions, the mill was again placed in operation and this nine-year-old catenary system was rebuilt with scarcely any loss in line material. Naturally, the electrical department was adept in taking down and rebuilding overhead systems, since this is daily procedure for the open-pit mine electrification. The original system is in operation today.

Supporting Structures for Permanent Track

Supporting poles are either wood or steel. At the Utah Copper Company mines, wood poles are used on permanent haulage, with the exception of the yards. Wood poles have been used extensively at the Canadian Johns-Manville mines at Asbestos, Que. Long outside haulage tracks of the International Nickel Company, near Sudbury, Ont., are equipped with wood poles for supporting light catenary overhead. The

Figure 16. This catenary span reduces the number of main-catenary hangers to a minimum



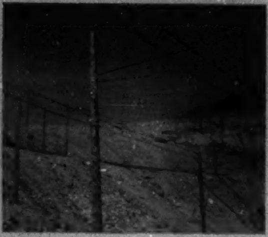


Figure 17. Compound catenary on permanent track at Morenci

use of wood depends entirely on the permanency of the electrification and the cost of the poles; however, at the Katanga mines in the Belgian Congo, wood is prohibited because of the destruction caused by ants. On tangent, the wood pole spaced 150 feet and supporting light catenary which does not exceed $1\frac{1}{2}$ pounds per foot can be used without guys, whereas longer spacings with heavier mechanical loads require guying.

Some of the yards at Utah are built with fabricated steel structures supporting cross spans or cross catenary, whereas the large storage yard of the Nevada Consolidated at Santa Rita, N. Mex., is built with wide-flange beams. The entire McGill electrification of the Nevada Consolidated is also built with wide-flange beams (Figures 19 and 20). Tubular steel poles, consisting of five-inch standard pipe, are used for permanent track on the Belgian Congo installation. These tubes are embedded in concrete. Steel poles, whether fabricated structures or shapes, are mounted on footings with anchor bolts at Utah and Santa Rita (Figure 21). At McGill, all steel poles are embedded, the footings having first been cast with a center well into which the pole is placed and subsequently grouted. The Utah fabricated poles are galvanized.

Figure 18. Crusher-yard catenary at Utah Copper Company was removed from this area after nine years of operation and later reinstalled with practically no loss of equipment

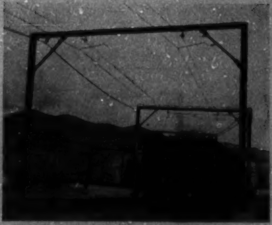


Figure 19. Light catenary installed on wide-flange portal structures is used for permanent track at Santa Rita

Substations

The most modern substation equipment is to be found at open-pit electrifications. At Santa Rita, the d-c substation includes two 1,000-kw rectifiers and an automatic switchboard. Normally, one rectifier operates on loads of 1,500 amperes or less. When the load exceeds 1,500 amperes for a period of two minutes, the second rectifier cuts in and will remain on the line until the load drops below 1,500 amperes for a 50-minute period. The railway system is divided into four sections. Each section is fed separately and is protected with 1,500-ampere breakers. At Morenci, four 1,000-kw rectifiers supply the traction energy. The Katanga operation has two rectifier substations, one on each end of the pit. Utah has three substations with a total capacity of 9,000 kw. These substations are located at the various load centers throughout the mine.

The ratios of substation capacity to connected load for some of the large strip operations are:

Utah	19 per cent
Morenci	22 per cent
Santa Rita	24 per cent

From these low ratios it can be seen that the various operations making up the haulage requirements are intermittent, and, although a locomotive is in service 24 hours each day, it may actually operate only 20 per cent of the time. As an example, the loading operation for a train may require 60 minutes, but the total time required by the locomotive for inching the cars in this 60-minute interval can be as low as three minutes. Consequently, the locomotive is in operation just five per cent of the loading time. Naturally, the haul from the mine to the crusher increases the overall ratio, but in the case of the Utah Copper Company at Bingham, Utah, the ratio is quite low, indicating low cost of conversion equipment.

D-C Feeder

Since each bench track and each dump track may be considered as a separate electrification, it may be concluded that a bench track that is served by only one locomotive cannot be equipped with feeder that has been calculated by using the ratio of installed substation capacity to the installed locomotive capacity. The single locomotive operates alone. Its load cannot be averaged with the

Figure 20. Yard catenary at Santa Rita. Columns are bolted to footings

Figure 21. At McGill, Nev., wide-flange columns are embedded in concrete footings cast with center well

loads of many other locomotives; therefore, the ratio used in calculating d-c feeder is much higher than the over-all mine ratio. Since the locomotives on a bench track or waste dump operate at very slow speeds, usually about three miles per hour, the current collected with the pantograph is not apt to reach anything like full load current. In fact, the current is usually one-half to one-third full load current. As a consequence, the ratio considered, when a feeder is designed for the mine, is usually 33 per cent, or as high as 50 per cent, if it is possible to increase current by increasing speeds. The usual ratio for underground coal operations for determining d-c feeder for traction purposes varies from 33 per cent to 45 per cent.

In general, comparatively little d-c feeder is necessary for the loads encountered. Very little longitudinal feeder is installed, since the many paralleling tracks on benches and yards provide the necessary copper. However, transverse feeding is quite common and is necessary to cross connect the trolley wires on a group of paralleling tracks. Section insulation is inserted at advantageous points where it is necessary, either to break the feeding or to protect the system against interruption of service because of some fault on one of the benches or on the waste dump. It has been the practice at the Utah Copper Company mine to feed a group of six locomotives on three benches from two sources of power. With this arrangement an interruption is localized and does not affect the entire operation. Yards, as a rule, are fed as one unit—insulation is not placed between tracks for segregation.

Bonds

Track bonds fall under two general types. For portable track a removable bond is used, and for permanent track a permanently attached bond is used. The removable bond is either the wedge type, which is a low-resistance copper-terminal bond that extends around the splice bar and is attached by driving a wedge into the web of the rail, or a temporary bond which also surrounds the splice bar and attaches to the base of the rail with large set screws.

The permanent track is bonded with short welded bonds, usually electric-welded, since the d-c circuit may be utilized both for the initial installation and for maintenance. A 4/0 bond is sufficient for rails in the 90–100-pound class, while the heavy main-line rail, usually 131 pounds, is connected with two 4/0 bonds. Cross bonds are placed at 500- to 1,000-foot intervals, depending on the type of track. Closer spacing is used on the temporary tracks (Figure 22).

Organization

The operators of large open-pit electrifications are to be commended on their ability to construct low-cost jobs. Fundamentally, the mine operator is not a man to split hairs with respect to the cost of equipment. Enormous tonnage and the necessity for low-cost haulage will prompt him to install adequate and reliable equipment and equipment that has been proved. As a consequence, one of the usual high items of expense is missing—engineering cost. The average engineering force for a large mine electrification rarely exceeds three, and these men are the regular electrical force of the mine. Outside consultation is secured from the manufacturer of equipment and from

Figure 22 (left). Gas-weld 4/0 bonds are applied to tracks at Santa Rita

Figure 23 (right). Construction and maintenance car at Morenci other mine operators. The mine operator does not insist on numerous gadgets and changes in design, but insists on dependable equipment as well as simple equipment. The job of moving thousands of tons of material daily is dependent on a minimum of breakdowns and, since mining has become a production-line process, transportation must function satisfactorily. Simplicity, a synonym for low cost, is the first rule of mining.

The nucleus of the construction forces is gathered from the mine. The foremen are usually acquainted with the designs and procedure before actual building starts. Therefore, the builders take an active interest in the equipment and in the construction and become trained maintenance men before actual operation is initiated. This procedure ultimately reduces maintenance costs and, to a great extent, the necessity for servicing (Figure 23).

Costs

When it is realized, that at one large strip mine the cost of water for steam locomotives paid the power bill after electrification, few additional data are necessary to convince the mine operator that electrification is economical. At McGill, Nev., the cost of haulage was reduced, by the substitution of electric energy for steam, from 10 cents to 3.4 cents per ton. At Morenci the cost of electric haulage is only two cents per ton, or slightly less than one cent per ton-mile, a figure that compares with the average cost per ton-mile on class 1 railroads.

Advantages of Electrification

A specific example of the advantages of open-pit electrification is found at Bingham. Here the number of loaded cars in an ore train has increased 25 per cent, and speed has increased 25 per cent. Therefore, fewer locomotives are necessary per shovel.

In general, with electric operation, there is less track wear due to the articulated locomotive, and there are fewer car derailments. With electrification there is no burning of wood crossties, an expense which was once tremendous in large mines. The electric locomotive does not require the transportation of fuel and water, an operation which added a large amount to the cost of haulage. Enginemen have greater vision while operating in and out of the pits, and the elimination of smoke helps pit operation, since good visibility is a big factor in the operation of a mine.

In conclusion, the electric locomotive is reliable. At the Bingham mine of the Utah Copper Company there are 65 locomotives, and there is rarely a time when more than two are in the shop. The electric locomotive is not affected by bad water, and its breathing apparatus is not susceptible to change in altitude. It consumes but one grade of fuel, and that is the best grade. The electric locomotive has proved a successful factor in the operation of large open-pit copper mines.

An Instrument for the Measurement of Large Alternating Currents

WALTHER RICHTER
FELLOW AIEE

Synopsis: An arrangement of the measurement and oscillographic recording of large alternating currents, consisting of a special air-core transformer and an amplifier, is described. The air-core transformer is wound on a flexible core of small cross section, permitting its easy linkage with the current to be measured without disturbing the circuit. The underlying theory of the air-core transformer is discussed, and the modifications necessary to make it suitable for measurement purposes are described.

THE measurement of large alternating currents, as required on the low-voltage side of resistance and flash welding transformers for instance, is an important problem, and many suggestions for satisfactory solutions have been made.[1]

The ordinary current transformer, such as used in almost all a-c measuring circuits, operates on the principle that in any iron-core transformer working with a low magnetic flux density in the iron core the total excitation resulting from the two windings is nearly zero. This means that the ampere turns produced by the meter winding equal the ampere turns or the amperes, in the case of a single conductor, of the current to be measured. Since the current density in the meter winding cannot, as a rule, be made higher than the current density in the primary conductor, which, it can be assumed, has been chosen as high as permissible with regard to the heating of the primary conductor, the copper cross section of the meter winding must at least equal the cross section of the primary conductor; the total space taken by the meter winding will be increased further by the insulation of this winding. In addition to this, the two windings must be made to link with a closed iron core. In low-voltage welding circuits every effort has been made usually to reduce the spread between the leads, or the "loop," to a minimum, and the insertion of a current transformer, which, for the reasons previously mentioned, necessarily will be bulky, is usually out of the question.

Suggestions have been made that a part of the welding circuit be used as a shunt for a meter. This scheme runs into difficulty from skin effects, especially for distorted wave shapes such as those that occur in flash-welding currents, and requires very careful placement of the leads to avoid inductive pickup. The calibration also presents some problems.

Air-core transformers have been used for the measurement of large alternating currents,[2] and this paper deals with a special arrangement of this type of transformer. Any air-core transformer is unsuitable for the measurement of currents without certain modifications, as will be evident from the following. Its operation is based on the fact that in the vicinity of a conductor carrying alternating current there will be an alternating magnetic field, the intensity of which will be proportional to the current, if no iron masses are close by. If a coil is placed in this field, a voltage will be induced in it, and this voltage may be used as an indication of the current. Since the induced voltage equals $M \times di/dt$, where M is the mutual inductance between the

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Walther Richter, now electrical engineer in the engineering development department of Allis-Chalmers Manufacturing Company, Milwaukee, Wis., was an independent consulting engineer at the time this paper was written.

The author expresses thanks for the suggestions of W. K. Crawford, E. J. Limpel, and A. Paalu of the A. O. Smith Corporation, the laboratory in which the device described in this paper was developed.

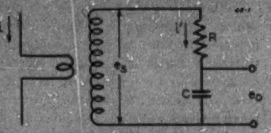

Figure 1. Circuit for electrical integration of an alternating voltage

primary conductor and the pickup coil, it is evident that the instantaneous values of this voltage are not proportional to the instantaneous values of the current, but rather to the instantaneous values of the rate of change of current. When a sinusoidal current of known frequency is dealt with, the voltage induced in the pickup coil will, of course, again be a sine wave, 90 degrees displaced against the current wave. In this case, the rms value of this voltage may be used safely as an indication of the current, as long as no phase relations are deduced from it. But let us assume now that the frequency of the current doubles, while its amplitude and rms value remain constant. Since di/dt doubles, the voltage in the pickup coil will also double. From this it is at once apparent that the air-core transformer fails to indicate the current by the induced voltage if the current has a distorted wave shape, that is, has harmonics. This is especially true in flash welding, but even in plain resistance welding the current may change in wave shape if electronic tubes are used to control the firing point in the cycle.

A second difficulty lies in the fact that the placement of the pickup coil determines the value of M and with it the magnitude of the induced voltage. This difficulty has been overcome to a large degree, however, by most of the workers in this field, by properly shaping and placing the pickup coil, and their method of approach has been utilized also in the present arrangement.

After the two essential problems of the air-core transformer have been stated thus, the methods of solving them will be discussed.

The voltage $e_s = M \times di/dt$, induced in the pickup coil, is a result of a differentiation of current, as the formula indicates. If the reversed operation, that is, an integration, is performed on it, the resulting wave will be a replica of the original current wave. This integration can be carried out electrically by the simple circuit shown in Figure 1. Let the periodic voltage e_s be applied to the series combination of R and C, and let the value of C be such, that its reactance is small compared with the value of R,

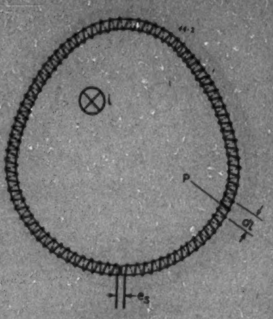

Figure 2. Linkage of toroid with current to be measured

even at the fundamental frequency of e_s. Under this condition the current through R and C will be determined essentially by the value of R only; that is, we can write without serious error

$$i' = \frac{e_s}{R} = \frac{M}{R} \times \frac{di}{dt} \qquad (1)$$

The voltage across the capacitor will be

$$e_0 = \frac{Q}{C} = \frac{\int i'dt}{C} = \frac{M}{R \times C} \times \int di = i \times \frac{M}{R \times C} \qquad (2)$$

Figure 3. Amplifier and integrating circuit

V1—6F5 tube
V2—6N7 tube
V3—2A3 tube
V4—80 tube
C1—0.5-microfarad capacitor
C2—24-microfarad capacitor
C3—1-microfarad capacitor
C4—100-microfarad capacitor
C5—16-microfarad capacitor
C6—50-microfarad capacitor
R1 —150,000-ohm resistor
R2 — 10,000-ohm resistor
R3 —250,000-ohm resistor
R4 — 5,000-ohm resistor
R6 — 50,000-ohm resistor
R7 — 20,000-ohm resistor
R8 — 10,000-ohm resistor
R9 — 10,000-ohm potential divider
R10— 375-ohm resistor

It must, of course, be realized that for this relation to be strictly true, the capacitor would have to be infinitely large, so that all the voltage e_s appears across R. However, the larger C, the smaller the output voltage e_0, and the higher the amplification will have to be, before use can be made of this voltage. In the present instrument $R = 100{,}000$ ohms, $C = 0.5$ microfarad, which makes the reactance of the capacitor at 60 cycles per second roughly five per cent of the value of the resistance; this seemed to be a fair compromise between accuracy and amplifier requirements.

To make the voltage induced in the pickup coil as nearly independent as possible of the position of the coil, it must have the proper shape and must be placed according to certain fundamentals. The basic law of electromagnetism states that the line integral of the tangential component of the magnetic-field intensity around any closed path equals 0.4 π times the exciting ampere turns linking with the path, that is

$$\oint H_t dl = 0.4\pi i \qquad (3)$$

where H_t is the component tangential to the path around which the integration is to be performed.

Suppose we surround the current with a toroidal* coil, wound on a flexible core, a length of heavy-walled rubber tubing, for instance. The winding is distributed uniformly over the whole length. Let n' be the number of turns per centimeter, so that the total number of turns N is equal to $n' \times 1$, where 1 is the length of

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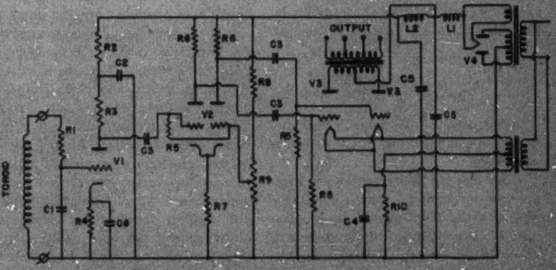
*The terms toroid and toroidal are used here and in the following for the sake of brevity, although the coil described here does not have to be a true toroid. A toroid is defined as the surface generated by the rotation of a plane closed curve around an axis lying in its plane. In this paper we mean a coil wound on a surface generated by moving a plane closed curve along a closed path in such a manner that its plane is always perpendicular to the direction of the motion, which is a cumbersome way of saying: Take a heavy-walled rubber tube, make a "doughnut" out of it, and place a uniformly distributed winding on it; then deform the "doughnut" any way you like (as long as you do not squeeze its cross section).



the tubing. The cross section of the core, that is, of the tubing, is held small enough so that the average magnetic density over the whole cross section can be considered, without serious error, equal to the magnetic density existing along the axis of the toroid at any given point P. Since the plane of each individual turn is essentially at right angles to the axis, a voltage caused by a changing magnetic field will be induced only by the component tangential to the axis. If A is the area of one coil turn, which in most practical cases will be very nearly equal to the cross section of the flexible core, in square centimeters, the voltage induced over the length dl of the coil will be

$$de_s = A \times n' \times dl \times \frac{dH_t}{dt} \times 10^{-8} \text{ volts} \tag{4}$$

and the voltage produced by the whole coil, forming a closed path, will be

$$e_s = A \times n' \times \oint \frac{dH_t}{dt}\, dl \times 10^{-8} \text{ volts} \tag{5}$$

Differentiating equation 3 with respect to time results in

$$\oint \frac{dH_t}{dt} \times dl = 0.4\pi \frac{di}{dt} \tag{6}$$

and substituting this into equation 5 gives

$$e_s = A \times n' \times 0.4\pi \frac{di}{dt} \times 10^{-8} \text{ volts} \tag{7}$$

which is independent of the position and length of the loop.

To obtain an idea of the order of magnitude of the voltages that can be expected, suppose we wind number 30 wire on a rubber tubing of one square centimeter cross section (about $^{7}/_{16}$ inch diameter). Number 30 enamel will give about 90 turns per inch, or 35 turns per centimeter, if we use a single-layer winding. Suppose that a sinusoidal current of 1,000 (rms) amperes with a frequency of 60 cycles per second is made to pass through a toroid made of this tubing. Considering the fact that the maximum rate of change of a sinusoidally varying quantity equals $2\pi f$ times the amplitude, the rms value of the induced voltage will be

$$\begin{aligned} e_s &= 1 \times 35 \times 0.4\pi \times 1{,}000 \times 2\pi 60 \times 10^{-8} \\ &= 0.166 \text{ volt} \end{aligned} \tag{7a}$$

The production of one volt will, therefore, require about 6,000 amperes at 60 cycles per second.

However, the voltage calculated according to equation 7 under the assumption of a sinusoidal current, as in equation 7a, is not the one to be measured, as explained earlier; the integrating circuit not only reduces the actual voltage available for measuring to about five per cent of the voltage induced in the toroid, but also makes it appear across a circuit element having a high impedance compared with the impedance of the usual low-range rms voltmeter. It is, therefore, quite obvious that an amplifier will be needed to accomplish the measurement. It is of importance that this amplifier have as flat a response as possible, if the output of it is to be used for the operation of an oscillograph, although there is nothing gained by extending the frequency range beyond that of the oscillograph. For a string oscillograph a flat response of the amplifier from 60 to 3,000 cycles will, therefore, be entirely satisfactory. Figure 3 shows the diagram of an amplifier which has been found satisfactory for the purpose. Further refinements, such as inverse feedback, can, of course, be introduced to increase the accuracy of the measurement or to improve the stability and permanency of calibration. The need for these will be dictated by the nature of the problem with which one has to deal. Examination of the diagram shows that the integrating circuit is incorporated into the input circuit of the amplifier. The voltage across the integrating capacitor is applied to a 6F5; the output of this tube is fed to a 6N7 which serves as amplifier and phase inverter, so that the pair of output tubes, 2A3's, receives essentially a push–pull signal. The secondary winding of the output transformer has several taps, so that a proper match with the load can be accomplished. Any high-quality standard amplifier, to which the special integrating circuit has been added, can, of course, be used in place of the one shown.

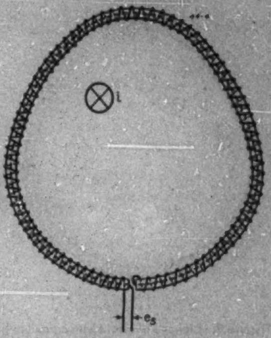


Figure 4. Arrangement of return lead to eliminate effect of magnetic fields not due to current to be measured

Calibration of the device is relatively simple. In contrast to a shunt method, it is not necessary to have a calibrating current available of the same order of magnitude as the currents to be measured. By linking the calibrating current several times with the toroid coil, its magnitude can be reduced to almost any desired value. Thus, if, for instance, a shunt coil of a d-c machine with a known number of turns is available, currents in the order of a few amperes can be used for calibration. Instead of using a known current for calibration, it is also possible to calculate the voltage which will be induced by a given current of sinusoidal wave shape and known frequency, as shown in equation 7a. For the toroid to which this equation refers, we had found a voltage of 166 millivolts induced by a 1,000 ampere rms, 60-cycle per second sinusoidal current. By applying 166 millivolts, 60 cycles per second, obtained from any convenient 60-cycle source (such as a voltage divider) to the given integrating circuit and amplifier, and observing the output of the amplifier, calibration is obtained. Because of the action of the integrating circuit, as explained previously, the calibration will be correct for any wave shape.

An interesting detail in connection with the leads of the toroid coil should be pointed out. During the initial experiments, when the leads were brought out, as in Figure 2, it was noticed that the voltage induced in the coil was not independent of the position of the coil with respect to the conductor. At first it was thought that this was due to the cross section of the toroid not being small enough. The true explanation, however, is that the toroid itself forms a loop, and any field will induce a voltage in it, regardless of whether a current passes through it or not. As soon as this was recognized as the cause, the remedy was obvious: one end of the winding should be brought back along or, ideally, on the axis of the helix, as shown in Figure 4. As soon as this was done, the position of the loop no longer had influence on the magnitude of the induced voltage. Another way to accomplish the same result, at the same time increasing the output voltage, is to wind another layer of turns on the toroid, progressing with this layer in the opposite direction.

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Design Features of Generators for Diesel-Electric Switcher Locomotives

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Synopsis: Simplicity and reliability have been kept foremost in generators recently developed for Diesel-electric switcher locomotives ranging from ten to 80 tons. New motors and control equipment have been developed at the same time. This paper deals especially with the generators, their electrical and mechanical features, and performance characteristics, both as power generators and as motors for engine starting from the locomotive battery. Operating fundamentally as self-excited generators, separate exciters have been eliminated. The simplicity of the generator windings has allowed corresponding simplicity of control circuits.

THE Diesel-electric-locomotive generator is part of a portable power plant. This power plant is built into the locomotive, and, if it does not function correctly, the locomotive does not do its work properly. This power plant must be reliable and controlled easily under variable conditions of weather, location, motion, and frequently varying load.

Mechanical and electrical simplicity and durability are very desirable features if one wishes to obtain continued and reliable service with operators that are not always acquainted thoroughly with electrical equipment. Simplicity of the control scheme is also dependent mainly on the generator performance characteristics and windings.

The purpose of this paper is to explain the electrical and mechanical features of some of the recent generators that have been developed for industrial switcher locomotives and railway switchers up to approximately 80 tons.

Mechanical Requirements

Generators with a single bearing at the commutator end have been adopted as standard for the sake of mechanical design simplicity. The armature is supported at the engine end by the engine crankshaft. This arrangement uses the smallest number of parts and provides for the closest coupling between the generator and engine.

Steel-disk couplings, flexible to angular misalignment, are employed on the higher-speed engine–generator combinations, where the generator stator is bolted solidly to an engine bell housing. The coupling flexibility takes care of slight inaccuracies in machining that may prevent the engine shaft and the generator shaft from being exactly in line. The steel-disk coupling does not have rotational flexibility. This is not desirable as the generator armature should act as a flywheel for the engine, thus allowing the engine flywheel to be cut down in weight merely to what is necessary for coupling purposes. When the generator stator is rigidly connected to the engine by being bolted to a bell housing, the whole power plant is mounted with a three-point support (see Figure 1). Two of these support points are at the sides of the generator frame, and the third point is at the opposite end of the engine. This three-point support makes it easy to provide a flexible mounting for the complete power plant when resilient supports at these three mounting points are supplied.

Another common method of combining the engine and generator into a unit power plant is that of supporting both on a rigid bedplate (see Figure 3). Means are provided for aligning the generator shaft accurately with the engine crankshaft before the generator frame feet are bolted solidly to the bedplate and doweled in position.

Figure 1. Engine–generator unit arranged for three-point support

1,800 rpm, 140-horsepower net input

The first impression that the layman gets, in looking at the engine–generator set for a Diesel-electric locomotive, is that the generator is much smaller than the engine. This, however, is not something for the generator designer to boast about. It is due to the inherent fact that the really active material of the engine requires a lot of attachments and

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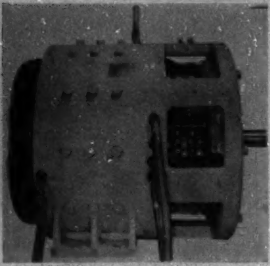


Figure 2. A generator used on 25- to 50-ton locomotives

1,000 rpm, 170-horsepower net input

auxiliaries for its proper functioning, whereas the electric generator is nearly all active material of either electric or magnetic circuits.

How large should a generator be to operate with a given engine? One engineer expressed it by saying that the generator should be able to do anything that the engine will do. This is a rough but simple way of stating it and is a very general guide. The Diesel engine and the d-c electric generator are such different types of power machines, however, that there is more to it than such a simple statement. More specifically, the generator size is determined usually by the torque and speed that the engine can supply to it continuously and by the maximum volts and maximum amperes demanded of the generator by the electric driving motors in order to meet the required locomotive performance. The most economical generator size is obtained when both it and the driving motors are designed at the same time, with proper regard to voltage and ampere values.

Naturally, generators directly connected to high-speed engines are smaller than those for slow-speed engines. Usually, engines with operating speeds below 1,500 rpm are called slow-speed, and those above 1,500, high-speed, for locomotive service.

Generators designed for this service are usually all new, so that there is a freedom of choice of the ratio of diameter to length. The result is always a compromise between the short and fat and the long and skinny types. This compromise is the result of consideration of the economical ratio of diameter to length of the armature core, the diameter that will best suit mounting with the given engine and a minimum over-all length of the engine–generator set. Another factor affecting this ratio is the critical speed of the rotating members of the engine and generator taken together. In some cases the diameter of the generator armature has been made smaller than it would have been, for the other reasons just mentioned, in order to reduce the armature WR^2 and keep away from critical speed vibrations that might cause dangerous stresses in the rotating parts.

This type of generator is well suited to effective self-ventilation by a fan on the armature shaft. The generator is nearly always mounted in a protected compartment, so that it is not necessary to enclose the space around the commutator other than to provide a cover over the top to protect the commutator and brush holders from falling objects. The arrangement shown in Figure 3 is especially effective in providing clean air for cooling the generator and using the air exhausted from it to assist in ventilating the engine compartment. A vertical partition in the enclosing hood (not shown in Figure 3) is located at the center of the generator body and prevents hot air from the engine compartment being used to ventilate the generator. In this case, the blower for force-ventilating the traction motors and control, the air compressor, and the auxiliary generator are arranged conveniently to be driven from a shaft extension at the commutator end without in any way preventing easy access to the brush holders and commutator.

Electrically, the generator must act as a generator for supplying power to the motors and also as a battery-driven motor for cranking the engine. These two types of electrical performance will be considered under separate headings.

Performance Characteristics as a Generator

When the performance curves of a Diesel-electric locomotive generator are discussed, we must always keep in mind the fact that it is supplied with a rather definitely limited amount of power input. Unlike the ordinary powerhouse lighting generator, the voltage cannot be kept constant over a wide range of ampere load. This is due to the fact that an increase in amperes, without a corresponding decrease in voltage, means an increase in power beyond the ability of the engine. The ideal generator-regulation characteristic is one that supplies just the right voltage at any ampere load and speed, so that its output plus the losses will balance exactly the power supplied to the generator. In other words, the problem of the generator regulation is to make its power input balance the output of the engine at any ampere load demanded by the traction motors and at any speed and throttle opening of the engine.

Many types of separate exciters and field-current regulators have been considered for properly adjusting the generator field strength at different ampere loads, so that just the right voltage is obtained. Many of these schemes are too complicated or introduce factors of unreliability or additional cost or maintenance to have found favor for use on small- and medium-size switcher locomotives.

The type of generator that gives a close approximation of the ideal regulation, and at the same time an ultimate in simplicity, is a modification of the ordinary self-excited d-c generator. The modifications consist of adding a small percentage of separate excitation from the locomotive battery and a special unsymmetrical shaping of the main pole faces. The unsymmetrical main-pole shaping is allowed, because the rotation is always in the same direction. The object of this pole-face shaping is to change the load saturation curves with respect to the

Figure 3. A locomotive power-unit arrangement that affords easy access and good ventilation for both generator and engine

1,000 rpm, 295-horsepower net input

Figure 4. 65-ton Diesel-electric railway locomotive

Figure 5. Ten-ton Diesel-electric industrial switcher locomotive

no-load saturation curve, so that the field excitation provided by the self-excited and battery fields will approach closely that required for the ideal voltage regulation. This effect results from a compounding action that counteracts the effect of armature reaction to the desired degree.

Figure 6 shows test voltage and speed regulations of a generator driven by a Diesel engine delivering 275 net horsepower to the generator at 1,200 rpm. From *F* to *E* on the speed curve, or *C* to *B* on the voltage curve, the generator is absorbing full-throttle power from the engine. Over this part of the curve the engine speed is determined by the load the generator demands from it. Since the speed is approximately constant, the horsepower input is also approximately constant.

At *E* the engine governor starts to operate, and the speed from *E* to *D* is determined by the governor and not the generator load. The part of the voltage curve *BA* is the voltage that the generator will produce at this governed speed with the same field setting as for the full-throttle operation from *C* to *B*. Point *B* is called the "unloading point." Most of the operation of a switcher locomotive will be over the voltage range from *C* to *B*, and, when maximum power and speed are required over this range, the operator will use wide-open throttle. Actually, in switching service, or even in so-called "transfer service," full throttle is not used for a very long period of time. At partial-throttle positions, both speed and voltage are lower at any given amperes. Since amperes represent locomotive tractive effort, partial throttle and lower voltage simply mean lower speed at a given tractive effort.

In cases where the locomotive is required to do high-speed service at light tractive efforts, it is desirable to provide a second field setting so that a higher-voltage unloading point will be obtained, as at *J*. This is obtained by decreasing the amount of resistance in the self-excited field circuit when the voltage has increased to *G*. This change is conveniently made with a voltage relay. This permits full-throttle loading with speed H_1–K and voltage G_1–J. At *K* the engine governor begins to operate, and at lower ampere loads the speed is determined by the governor, and the generator produces voltage *J*–*L*.

It is obvious that, if full power were taken from the engine at very low amperes, the voltage required would be very high. The minimum amperes for which the unloading point is set must be chosen with due respect to the maximum voltage that the generator commutator is good for and the voltage required by the traction motors to provide the specified high-speed performance of the locomotive.

It should be kept in mind that the performance shown in Figure 6 is the maximum performance of the power plant, and that most of the operation will be at less power and correspondingly lower speed and volts. This lower power performance will be a result of the operator not using full throttle. Diesel engines for locomotive service are not intended for full-throttle operation continuously. The generator could operate continuously between two limits of continuous rating. The upper limit of continuous ampere rating is determined by the heating of the main current-carrying parts, such as armature windings, commutating field windings, and commutator; the minimum limit, by heating of the self-excited field which has increased amperes and resulting increased heating as voltage rises.

The battery field uses only a few amperes from the battery and is left on at all loads. The purpose of this separately excited battery field is to provide a positive flux in the magnetic circuit and definite voltages at the lower operating speeds. The special shaping of the main pole faces is of assistance to the battery field in this respect.

Performance Characteristics as a Starting Motor

The most convenient method of engine cranking is to use the generator as a starting motor. This eliminates the necessity for a separate starting motor, its mounting and gearing to the engine flywheel.

A series motor characteristic is necessary to provide both high breakaway torque and the required firing speed and torque. A separate series main-field winding is built into the generator especially for this purpose. At starting, this field winding is connected in series with the armature and commutating field. This starting-motor circuit is connected directly to the battery terminals until the engine fires.

Engine builders are usually in a position to furnish information on the breakaway torque required to start the engine

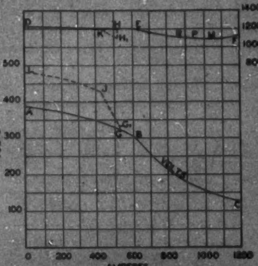


Figure 6. Typical generator-voltage and speed-regulation curves

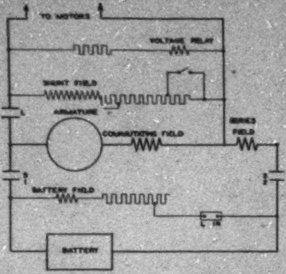


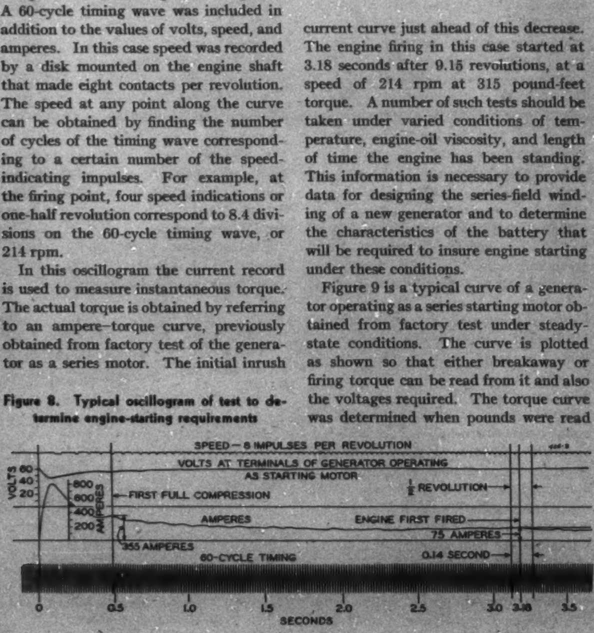
Figure 7. Schematic control diagram showing simplicity of circuits

from zero speed and the speed and torque, required at firing. The best method of obtaining this information, with all mechanically driven auxiliaries connected as in service, is by means of an oscillographic test recording volts, amperes, and speed of the generator acting as a starting motor. The values of speed, torque, current, and volts during starting are such rapid transients that ordinary meter readings are very uncertain.

Figure 8 is a tracing of a typical oscillogram of such an engine-starting test. A 60-cycle timing wave was included in addition to the values of volts, speed, and amperes. In this case speed was recorded by a disk mounted on the engine shaft that made eight contacts per revolution. The speed at any point along the curve can be obtained by finding the number of cycles of the timing wave corresponding to a certain number of the speed-indicating impulses. For example, at the firing point, four speed indications or one-half revolution correspond to 8.4 divisions on the 60-cycle timing wave, or 214 rpm.

In this oscillogram the current record is used to measure instantaneous torque. The actual torque is obtained by referring to an ampere–torque curve, previously obtained from factory test of the generator as a series motor. The initial inrush of current is not a measure of breakaway torque, since this current is limited only by the impedance of the circuit and would represent more torque than is required to start the engine turning. The ripples in the current curve indicate variations of torque due to the cylinder compressions. The maximum value of the first full compression has been taken as the current corresponding to maximum breakaway torque. This value, scaled from the oscillograph, is 355 amperes. From the ampere–torque curve for the generator used in this test, this current corresponds to a torque of 1,040 pound-feet.

Figure 8. Typical oscillogram of test to determine engine-starting requirements



After breakaway, acceleration continued up to a time of approximately 2.0 seconds, after which the speed was nearly constant, as indicated by the equal spacing of the speed-indicator impulses.

When the engine fired, a sudden decrease in current took place. The point is accurately determined when a tangent is drawn to the tops of the ripples of the current curve just ahead of this decrease. The engine firing in this case started at 3.18 seconds after 9.15 revolutions, at a speed of 214 rpm at 315 pound-feet torque. A number of such tests should be taken under varied conditions of temperature, engine-oil viscosity, and length of time the engine has been standing. This information is necessary to provide data for designing the series-field winding of a new generator and to determine the characteristics of the battery that will be required to insure engine starting under these conditions.

Table I

Commutating-Field Winding	Volts	Amperes
In	27.5	375
Out	34.5	325

Figure 9 is a typical curve of a generator operating as a series starting motor obtained from factory test under steady-state conditions. The curve is plotted as shown so that either breakaway or firing torque can be read from it and also the voltages required. The torque curve was determined when pounds were read at the end of an arm clamped to the shaft under locked condition. This was checked when torque was calculated from inputs, speeds, and losses while it ran at 30 volts. Theoretically, the torque, when running, should be slightly less than the locked torque, but the speed is low and the field strength comparatively low in comparison with the operating conditions as a generator, so that the rotational losses are practically negligible. The running and locked torques are, therefore, plotted as one curve for simplification.

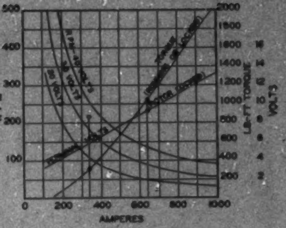


Figure 9. Typical curve of generator operating as a series starting motor

To illustrate the use of this curve, let us use the values of torques and speed obtained from the oscillograph test and determine what values of volt and amperes this generator will require to start the engine. (Incidentally, the oscillograph test was taken on a different type of generator prior to the development of this one.) The breakaway torque of 1,040 will require 640 amperes and 9.2 terminal volts. The firing torque of 315 at 214 rpm will require 335 amperes and a terminal voltage of 35. The latter value is obtained by interpolating between the 30- and 40-volt speed curves.

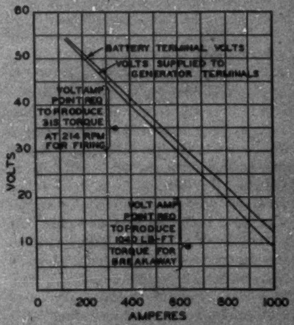


Figure 10. Battery volt-ampere curve with volt-ampere points for a given engine's starting requirements

Figure 10 shows these volt-ampere points located on the same plot with the voltage of a proposed battery. The battery-voltage curves were obtained from the battery manufacturer's data. Such data are usually given in volts per cell and amperes per positive plate. It is plotted here as total amperes and terminal volts for the battery considered. The volts drop from battery to generator terminals must be accounted for. This drop includes that of such switches and contactors as are in the starting circuit. It is advisable to utilize liberal cable size in this circuit and to keep the lengths as short as possible, because a few volts drop multiplied by the amperes required represent a large percentage of the power being drawn from the battery.

If the volt-ampere points for breakaway and firing lie well below the generator-terminal voltage curve, then the battery proposed can be considered as adequate.

The question is sometimes asked as to why the commutating-field winding is not omitted when the generator is used as a starting motor at the comparatively low battery voltage. This would give a lower-resistance starting motor, and, at first thought, this would require less battery volts at a required speed and torque. Actually, this is not the case. For example, tests on a generator, used to start an engine which required a firing torque of 200 pounds-feet at 200 rpm, showed the volt-ampere requirements given in Table I.

Although the amperes required are 13 per cent less, the voltage is 25 per cent greater with the commutating-field winding omitted and would require a higher-voltage battery. The reason for this is the familiar effect of an undercompensated commutating field reducing the speed of a motor. In the case of complete omission of the commutating-field excitation with the poles still in place, the undercompensating effect is very marked and produces the unfavorable change in required volts and amperes shown in Table I.

Conclusion

Generators having the mechanical features and the electrical performance described in this article are being applied to many sizes of industrial and railway switching locomotives at the present time. In addition to use on railroads and in industrial plants, many are being used by the Army and Navy in the United States and overseas. Others are being supplied to our allies. Their simplicity and reliability will be important factors in prompt movement of supplies and men in the many locations where they will be used.

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Diesel-Electric Switching-Locomotive Application by Means of Nomographic Charts

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Synopsis: The application of switching locomotives involves calculations of locomotive performance under given conditions of grade, train weight, and length of run. Such calculations can be made quickly by nomographic charts. Equipment heating limitations on Diesel-electric and electric locomotives can also be checked by elements added to the nomograms. The theory and the method of construction of such charts are described in the paper.

In the process of determining applicability of Diesel-electric locomotives to railroad and industrial switching services, it is often necessary to determine whether or not the haulage of a given train up a grade of known length and severity is possible without exceeding the locomotive-equipment heating limitations.

The performance of such a haul is limited by wheel-to-rail adhesion, or by heating of the electric (traction motors and generators) or the engine equipment, or by both of these limitations.

Since switching service—be it industrial or railroad—usually comprises relatively short movements at full-load rating of the locomotive-engine equipment, coupled with idle periods, the engine equipment is, in general, applied on an intermittent-rating basis. Thus, a nominal 500-horsepower unit is suitable for sustained operation of the engine equipment at 500-horsepower output for but a limited period of time. This may be a period of one-half hour, or one hour, after which the load must be reduced to a fraction of the rating for a definite period of time before the nominal rating load can be carried again.

Likewise, the traction motors and generators are limited to definite-period loading, depending upon the heating characteristics of the machines under various conditions of loading.

The conventional speed–tractive-effort characteristic of the Diesel-electric switching locomotive, by itself, gives no information as to the service that can be performed with the locomotive without exceeding its equipment limitations, particularly in the high-tractive-effort low-speed portions of the characteristic where electric-equipment heating is limiting.

The speed–tractive-effort characteristic can be combined with the time-limitation characteristics, both of the engine and the electric equipment, on a nomographic chart which can be used to determine graphically and quickly whether or not a particular locomotive can handle safely a given train under known conditions of grade and length of grade.

The trailing tonnage that can be

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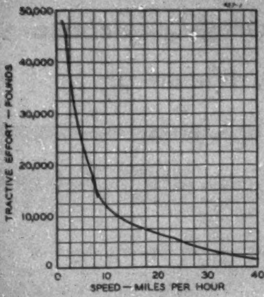

Figure 1. Speed–tractive-effort characteristic for 80-ton 500-horsepower Diesel-electric switching locomotive

handled by a locomotive at a definite speed on a given grade is determined by solution of the equation

$$W_t = \frac{T - W_L(R_L+20G)}{(R_t+20G)} \tag{1}$$

in which

T = tractive effort in pounds of the locomotive at the given speed
R_L = resistance of the locomotive in pounds per ton of locomotive weight at the given speed
R_t = train resistance in pounds per ton of train weight at the given speed
W_t = weight of trailing train in tons
W_L = weight of locomotive in tons
G = grade in per cent

The train resistance varies with the individual weights of cars in the train, but, since unjustifiable complications in the calculations would be involved were this variation considered, a fixed value of train resistance based on experience is assumed in the following. This is taken at 8.5 pounds per ton. The value of R_L, resistance of the lcomotive,[1] is, however, easily determined for different speeds by the equation

$$R_L = 1.3 + \frac{29}{W} + 0.03V + \frac{0.0024AV^2}{WN} \tag{2}$$

in which

R_L = resistance of locomotive in pounds per ton
W = weight per locomotive axle in tons
N = number of locomotive axles
V = speed in miles per hour
A = cross section of locomotive in square feet
= 105 for 50-ton locomotives
= 110 for 70-ton locomotives
= 120 for 100-ton locomotives

By means of the two equations, given in the foregoing, and the speed–tractive-effort characteristic curve applying to the particular locomotive (Figure 1 shows such a characteristic for a 500-horsepower 80-ton locomotive), a speed, grade, and trailing tons can be indicated as in Table I.

This table can be plotted in the form of a three-element nomogram with speed, grade, and trailing tons as the three elements. While the nomogram may be worked out analytically, the authors suggest that the graphical method of constructing the nomogram, as described in the following, is simpler and less tedious for those not skilled in setting up nomograms in general.

On any convenient size of paper the G scale is drawn in at the left-hand side of the paper, perpendicular to the base ordinate, while the W scale is erected perpendicular to the base line near the right-hand side of the paper (Figure 2). The authors suggest a minimum distance of 10 inches between the G and W scales and a length of 20 inches for the G and W scales for charts for locomotives up to 1,000-horsepower rating.

The W scale is marked off in a rectilinear scale, so that the maximum tonnage shown in Table I falls near the top of the sheet with the value of 0 tons at the base line.

On the G scale the value of 1.0 per cent grade is arbitrarily located one half of the length of the G-scale line above the base line. The value of zero per cent grade is taken at the base line.

The S scale is located as follows:

From the table select a value of tonnage that can be handled at a low speed—5.0 miles per hour—on level grade, and draw the construction line AB (Figure 2). From the table select a value of tonnage, corresponding to the same speed previously used—5.0 miles per hour in the present case—, that can be hauled on a 1.0 per cent grade. Draw the construction line CD. The intersection of AB and CD determines one point on the S scale. A second point corresponding to a higher speed can be determined in a similar manner when two combinations of speed, tonnage, and grade are selected from Table I. The S scale is then drawn in, as shown, as a straight line.

The G scale may be calibrated up to the maximum value of grade desired when the value of W is calculated for assumed values of G at 5.0 miles per hour by equation 1, and when the values of G are located on the scale by means of a straightedge from the value of W on the W scale through the 5.0 mile-per-hour point on the S scale.

Similarly, the S scale may be calibrated by laying a straightedge between the values of tonnage and grade given in Table I.

The upper end of the S scale may be taken at a speed point corresponding to 25 per cent adhesion tractive effort on the speed–tractive-effort characteristic. In the example selected 25 per cent of the locomotive weight is 40,000 pounds and the speed corresponding to this tractive effort from Figure 1 is 2.2 miles per hour.

The elements of the chart, which are required to indicate engine- and electric-equipment heating limitations, are added as described in the following procedure.

At the point on the S scale corresponding to 25 per cent adhesion speed, a perpendicular AB in Figure 3 is erected. Another line CD, Figure 3, is erected perpendicular to the S scale at the locomotive speed corresponding to the short-time limitation on the electric equipment which corresponds to the short-time limitation on the engine equipment. In the example selected, the engine equipment is limited to operation at 500 horsepower for 30 minutes, and the speed of 3.5 miles per hour corresponds to a 30-minute rating on the electric equipment for an initial winding temperature of 100 degrees centigrade and a final winding temperature (at the end of 30 minutes) of 160 degrees centigrade.

The point D is projected over to the line AB, parallel to the S scale to the point E. The line AE is then laid off on a logarithmic scale in minutes with the value of one at A and 30 at E.

The points F, G, and H are laid in on

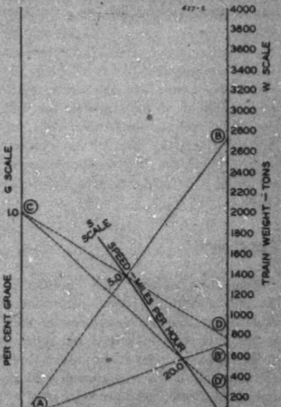

Figure 2. Construction of speed–grade–train-weight nomogram

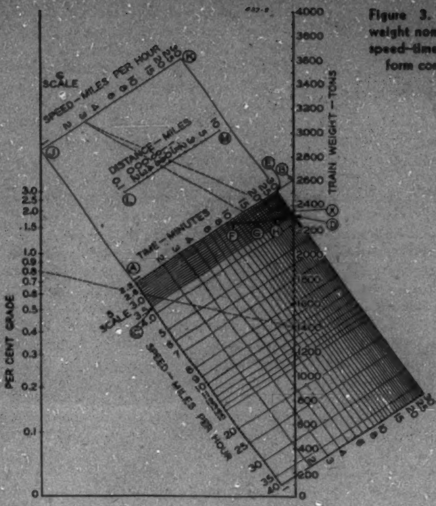

Figure 3. Speed–grade–train-weight nomogram combined with speed–time–distance nomogram to form complete working chart

the 10-, 15-, and 20-minute lines at the speeds corresponding to the tractive efforts to which these short-time ratings conform. The line *FGHD* indicates the limitations imposed by safe heating of the electric equipment.

The line *JK* is now drawn in as shown, perpendicular to the *S* scale at *J*, and calibrated in a log scale in speed in miles per hour with the value of one at *J* and the value of 30 directly opposite the 30-minute mark on *AB*.

The line *LM* is erected midway between *JK* and *AB* and calibrated in a log scale in miles fixing the 0.1-mile and 10-mile points with a straightedge laid between *JK* and *AB* at the points three miles per hour and two minutes (for 0.1 mile) and 30 miles per hour and 20 minutes (for ten miles).

The elements *AE*, *LM*, and *JK* are a nomographic plot of the equation

$$M = 0.01667 \times S \times T \qquad (3)$$

in which

M = distance in miles
S = speed in miles per hour
T = time in minutes

The chart is now ready for use.

As an example of its use, it is desired to determine whether the haulage of a 1,400-ton train up an 0.8 per cent grade one mile long will cause overheating of the engine or electric equipment on the 80-ton 500-horsepower locomotive for which the chart has been drawn.

A straightedge laid between 0.8 on the *G* scale and 1,400 on the *W* scale crosses the *S* scale at 2.8 miles per hour.

A straightedge laid across the points 2.8 on line *JK* and 1.0 on line *LM* intersects the time scale at 21.5 minutes. A line drawn from 2.8 on, and perpendicular to, the *S* scale intersects the 21.5-minute line at *X*, and, since this point lies above the electric-equipment heating line *FGHD*, the indication is that the electric equipment will be overheated in performing the given service. It may be noted that the line from 2.8 miles per hour on the *S* scale to *X* intersects the line *FGH* at 14 minutes. A straightedge, laid between the 14-minute point on line *AB* and the 2.8 point on *JK*, intersects the *M* scale at 0.66 mile, indicating that haulage of the 1,400-ton train on an 0.8 per cent grade should be limited to a distance of 0.66 mile, if the electric equipment is not to be overheated.

By obvious cut-and-try methods it can be determined that a train of 1,285 tons trailing is the maximum that can be handled on the 0.8 grade, one mile in length, without exceeding the permissible temperature rise in the electric equipment.

If the intersection of the speed line from the *S* scale with the time line falls below the electric-equipment heating-limitation line, but to the right of the 30-minute line, overheating of the engine equipment is indicated.

If the straightedge between the *G* scale and *W* scale falls across the *S* scale at a point above and to the left of the point *A*, a high value of adhesion (greater than 25 per cent) and the likelihood of wheel slippage are indicated.

Table I. Trailing Tonnages

Speed Miles Per Hour	Level	Per Cent Grade (Compensated) 0.5	1.0	1.5	2.0
3.3	3,737	1,674	1,057	762	588
5.0	2,767	1,227	768	547	418
7.5	1,928	840	516	361	270
10.0	1,420	609	367	251	183
15.0	974	404	234	152	104
20.0	731	292	161	98	61
25.0	540	204	104	56	28
30.0	340	113	45	12	
35.0	193	45			
40.0	100				

Resistance based on 8.5 pounds per ton, motors blown.

Tonnages do not include weight of locomotive.

Reference

1. Tractive Resistance of Electric Locomotives and Cars, W. J. Davis, Jr. *General Electric Review*, October 1926.

Symmetrical-Components Analysis of the Four-Phase System

A. BOYAJIAN
FELLOW AIEE

Synopsis: Symmetrical-components concepts are developed for the currents, voltages, and impedances of a four-phase system, and formulas are given for various types of short circuits. The idea of symmetry among the members of each set is based on the angular displacement between the members of the set, rather than that of phase rotation which fits the three-phase case very nicely but is inadequate for the four-phase case.

Aside from its possible value for four-phase problems, which are rather rare, the paper is presented here in the expectation of an additional benefit, namely, that the parallelisms and contrasts between the four-phase and the three-phase formulas may throw further light on the general theory and applications of symmetrical components.

THE highly developed automotive art—model of American efficiency in production—has taught the public to expect that the factory must be specially tooled up for each new model. The philosophy of tooling is such common knowledge in the mechanical arts that, even though many an operation could be carried out with hammer, file, and screw driver, many a mechanic will frequently be seen making a special tool for an odd operation, as requiring less time in the end. The same philosophic principle holds in all the arts, electrical as well as mechanical; and such an experience in electrical engineering, with a conscious philosophy of tooling, led to the work which forms the subject matter of this paper.

The author had occasion to analyze the performance characteristics of an existing two-phase system if converted to a five-wire four-phase system. The attempt was made first by elementary methods, using such common tools as Kirchhoff's laws, but the problem involved unknown mutual impedances, and additionally the equations tended to be lengthy and burdensome, and each new problem meant going through a similar distasteful operation all over again. It was then thought that on the whole the work would be much less and the results more dependable if a tool was developed in the form of a four-phase symmetrical-components method of analysis. This was done, and the results fully justified the expectations. The analysis became very systematic and straight forward; the impedances, such as could be estimated with some confidence; and the resulting formulas, such as could be interpreted and checked physically. It was then thought that it might be worth while to put this system of analysis on public record, not only for use on those few occasions when four-phase system problems may be encountered, but also for its technical and educational value, as a further illustration of the power of the symmetrical-components concept as a tool in the analysis of electrical systems.

Those familiar with the three-phase technique will be interested in observing the resemblances and the differences between the three-phase and the four-phase techniques.[1-8]

Concept of Symmetrical Components in the Four-Phase System

The conventional concept of symmetrical components of three-phase system—positive-negative-, and zero-phase-sequence components—is peculiar to the three-phase circuit and not very adaptable to a system with greater number of phases, because a four-phase system must have four components (n-phase system n components), and the concept of positive-, negative-, and zero-phase-sequence components offers us no clue as to how to find an appropriate fourth component for the four-phase system.

If, instead of considering the direction of rotation as the distinguishing mark of a symmetrical set of vectors, we consider the magnitude of the characteristic angle between the vectors of the adjacent phases as the distinguishing mark, we obtain a rule that enables us to define the symmetrical components of a system of any number of phases. Thus, if the angle between adjacent phases is θ for the normal symmetrical set of vectors of the system, then a set in which the vectors succeed each other at intervals of 2θ constitutes a second symmetrical set; one with intervals of 3θ, a third symmetrical set; and so forth. The negative-sequence set of the three-phase system corresponds to the set having the characteristic angle of 2θ. Since, in the n-phase system, θ equals $360/n$ degrees, the nth set of symmetrical vectors will have the characteristic angle $n(360/n)$, or just 360 degrees, which is equivalent to zero; that is, all of the vectors will be in phase with each other, like the zero-phase-sequence set of the three-phase system. The set $(n+1)$ turns out to be a duplication of set 1, $(n+2)$ a duplicate of set 2, and so forth, so that the n-phase system has only n distinct symmetrical components.

Accordingly, in a four-phase system (Figure 5), the consecutive phases of the normal symmetrical set of vectors I_1 are spaced 90 degrees apart (Figure 1); those of set I_2, 2×90 degrees or 180 degrees apart (Figure 2); those of set I_3, 3×90 degrees or 270 degrees apart (Figure 3); and those of set I_4, 360 degrees, or what is equivalent to it, 0 degrees apart (Figure 4).

Evidently, the set I_1 (Figure 1) corresponds to the positive-phase-sequence set of three-phase systems: the set I_3 (Figure 3), to the negative-phase-sequence set; and the set I_4 (Figure 4), to the zero-phase-sequence set; but there is nothing like the set I_2 (Figure 2) in a three-phase system. The set I_2 has a similarity to the set I_4, except for the important difference that, instead of a neutral return, the horizontal phases act as return to the vertical phases, and vice versa.

The difference in concepts leads also to a difference in notation. If we wish the subscript to represent the characteristic angle, then the set of vectors in Figure 3,

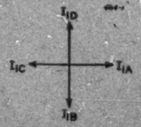

Figure 1. Symmetrical set of vectors, I_1

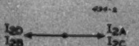

Figure 2. Symmetrical set of vectors, I_2

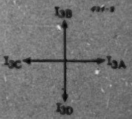

Figure 3. Symmetrical set of vectors, I_3

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Table I. Impedance Data

	Bank G 7,500-Kva Base (Per Cent)	Bank U 7,500-Kva Base (Per Cent)	Primary Lines 15,000-Kva Base (Per Cent)	Total 15,000-Kva Base (Per Cent)
Z_1	6.4	6.4	2.4	8.8
Z_2	75	75		75
Z_3	6.4	6.4	2.4	8.8
Z_0	75	Infinity		150

with the characteristic angle 3θ, is correctly marked as I_3; whereas, if phase rotation were to be the guide, we would have had to mark it I_2, and the set I_4 as I_0, conforming to the conventional notation for the negative- and zero-phase-sequence components of the three-phase circuit. This author feels that the three-phase notation is about as poorly adapted as the three-phase concept to the four-phase circuit, but, inasmuch as I_4 has zero characteristic angle in all systems, we may use the conventional symbol I_0 for it. So in what follows, we shall refer to I_4 and E_4 as I_0 and E_0.

Impedances to Sequence Currents

The types of symmetrical sets of currents already defined, and illustrated in Figures 1–4, define also the corresponding impedances (Z_1, Z_2, Z_3, Z_0) for a four-phase system.

(*a*). Z_1 is the impedance of the circuit to the normal balanced four-phase currents of Figure 1. It is independent of the condition of the neutral—whether it be grounded or isolated, and even whether the mid-point of the vertical windings is joined with that of the horizontal windings. Phase *A*, however, must be joined with *C*, and *B* with *D*.

(*b*). Z_2 is the impedance offered to currents like those in Figure 2. As the horizontal and the vertical phases act as return circuits to each other, their mid-points must be tied together. Otherwise Z_2, will be infinity. The grounding or isolation of the common neutral makes no difference to Z_2. Z_2 can be measured with single-phase voltage, connecting *A* to *C* as one terminal, and *B* to *D* as the other. The result must, of course, be expressed per phase.

To secure a low value of Z_2, opposite phases must be well interlaced or closely coupled with each other. Depending on the presence or absence of such treatment in transformers, Z_2 may be as low as $Z_1/10$, or as high as 10 Z_1.

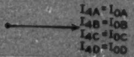

Figure 4. Symmetrical set of vectors, I_0

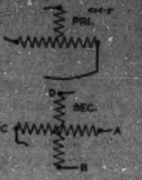

Figure 5. Four-phase supply system—three-phase primary, four-phase secondary connections

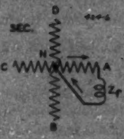

Figure 6. Single-phase load, line-to-neutral

(*c*). Z_3 is the negative-phase-sequence impedance and has the same value as Z_1 for static circuits.

(*d*). Z_0. If Figure 4 is compared with Figure 2, there is a certain similarity in the flow of currents, I_2 and I_0, by virtue of which, Z_0 may have the same, or approximately the same, value as Z_2. The important case in which this would not be true at all is the case of the isolated neutral, in which case I_0 cannot flow, and Z_0 must then be set down as infinity, while Z_2 may still have a very low value.

In most of the practical instances, therefore, the four-phase system is likely to have only two independent impedances, Z_1 and Z_0; although, in the general case, the static system will have three independent impedances, Z_1, Z_2, and Z_0; and the rotating machine, four.

In all of these cases, it is necessary to assume that, for any one set of currents, the corresponding set of impedance drops are also symmetrical in the same sense as those currents. If this is far from true, the value of symmetrical-components method of analysis is greatly reduced.

Symmetrical Components of Unsymmetrical Four-Phase Currents

Representing the four phases by letters, the symmetrical components by numbers, and with phase *A* as the reference phase,

$$I_1 = I_{1A} = (I_A + I_B\underline{/90^\circ} + I_C\underline{/180^\circ} + I_D\underline{/270^\circ})/4 \qquad (1)$$

$$I_2 = I_{2A} = (I_A + I_B\underline{/2\times90^\circ} + I_C\underline{/2\times180^\circ} + I_D\underline{/2\times270^\circ})/4 \qquad (2)$$

$$I_3 = I_{3A} = (I_A + I_B\underline{/3\times90^\circ} + I_C\underline{/3\times180^\circ} + I_D\underline{/3\times270^\circ})/4 \qquad (3)$$

$$I_0 = I_{4A} = (I_A + I_B\underline{/4\times90^\circ} + I_C\underline{/4\times180^\circ} + I_D\underline{/4\times270^\circ})/4 \qquad (4)$$

and inversely

$$\left.\begin{aligned} I_A &= I_1 + I_2 + I_3 + I_0 \\ I_B &= I_1\overline{/90^\circ} + I_2\overline{/2\times90^\circ} + I_3\overline{/3\times90^\circ} + I_0\overline{/4\times90^\circ} \\ &= I_1\overline{/90^\circ} - I_2 + I_3\underline{/90} + I_0 \\ I_C &= I_1\overline{/180^\circ} + I_2\overline{/2\times180^\circ} + I_3\overline{/3\times180^\circ} + I_0\overline{/4\times180^\circ} \\ &= -I_1 + I_2 - I_3 + I_0 \\ I_D &= I_1\overline{/270^\circ} + I_2\overline{/2\times270^\circ} + I_3\overline{/3\times270^\circ} + I_0\overline{/4\times270^\circ} \\ &= I_1\underline{/90} - I_2 - I_3\underline{/90} + I_0 \end{aligned}\right\} \qquad (4.1)$$

These accord with Figures 1–4.

Equations analogous to 1–4.1 apply also to voltages.

We now apply these formulas to some representative unsymmetrical cases for use in short-circuit calculations.

SINGLE-PHASE LOAD FROM ONE LINE TO NEUTRAL: *A–N*

As line *A* (Figure 6) is the only one with current, all others zero,

$I_A = I_A$ (given); $I_B = 0$; $I_C = 0$; $I_D = 0$

Therefore, in applying formulas 1–4, only the first term on the right needs consideration, and we obtain the result

$$I_1 = I_A/4;\ I_2 = I_A/4;\ I_3 = I_A/4;\ I_0 = I_A/4 \qquad (5)$$

That is, in the loaded phase (phase *A*), all four components are equal and in phase. It can be verified that in the adjacent phases (*B* and *D*, idle), I_2 cancels I_0, and I_1 cancels I_3. In the opposite phase (*C*, also idle), I_1 cancels I_2, and I_3 cancels I_0. These relationships find a significance in the calculation of voltage drops. If the corresponding impedances are alike, where the currents cancel, so will the voltages; but, if the impedances are dissimilar, a net voltage change (with no net current) will result.

Table II

	Grounded Bank	
	Phase A (Per Cent)	Phase C (Per Cent)
(1) I_1Z_1 (1.65/4) 8.8 =	3.63	3.63
(2) I_2Z_2 (1.65/4) 75 =	31	−31
(3) I_3Z_3 (1.65/4) 8.8 =	3.63	3.63
(4) I_0Z_0 (1.65/4) 150 =	62	−62
(5) Total Drop	100	−85.7

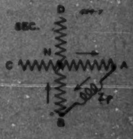

Figure 7. Single-phase load, line-to-line

SINGLE-PHASE LOAD ACROSS ADJACENT LINES: *A–B*

It is seen from Figure 7 that

$I_A=I_A$ (given); $I_B=-I_A$; $I_C=0$; $I_D=0$

Hence, only the first two right-hand terms of formulas 1–4 count, and we obtain the results

$$\left.\begin{array}{ll} I_1=\frac{1}{2\sqrt{2}}I_A/\overline{45^\circ} & \text{(a)} \\ I_2=\frac{1}{2}I_A & \text{(b)} \\ I_3=\frac{1}{2\sqrt{2}}I_A/\underline{45^\circ} & \text{(c)} \\ I_0=0 & \text{(d)} \end{array}\right\} \quad (6)$$

SINGLE-PHASE LOAD ACROSS OPPOSITE LINES: *A–C*

From Figure 8

$I_A=I_A$ (given); $I_B=0$; $I_C=-I_A$; $I_D=0$

By equations 1–4

$$\left.\begin{array}{l} I_1=0.5I_A \\ I_2=0 \\ I_3=0.5I_A \\ I_0=0 \end{array}\right\} \quad (7)$$

Formulas for Fault Currents and Voltages

LINE-TO-NEUTRAL SHORT CIRCUIT: *A-N*

If the load in Figure 6 is treated as a short circuit, the entire phase voltage E_{NA} is then consumed by the total impedance drop in phase *A*. Hence

$$\overrightarrow{E_{NA}}=I_1Z_1+I_2Z_2+I_3Z_3+I_0Z_0$$

If the values of I_1, and so forth, are substituted from equation 5,

$$E_{NA}=I_A(Z_1+Z_2+Z_3+Z_0)/4 \quad (8)$$

As I_A is now I_{fault}, therefore

$$I_{\text{fault}}=4E_{NA}/(Z_1+Z_2+Z_3+Z_0) \quad (9a)$$

and for static circuits

$$=4E_{NA}/(2Z_1+Z_2+Z_0) \quad (9b)$$

If the fault has an impedance Z_f, an impedance Z_f may be assumed in series with each one of the open lines also for symmetry, in which case each one of the impedances $Z_1, \ldots, Z_0$ will be increased by Z_f, changing equation 9a into equation 10

$$I_f=4E_{NA}/(Z_1+Z_2+Z_3+Z_0+4Z_f) \quad (10)$$

Another way of considering the fault impedance in this case is as one connected in series with the neutral, carrying a current $4I_0$, in which case, Z_1, Z_2, Z_3 are not affected, but Z_0 is increased by $4Z_f$, leading again to equation 10.

This equation 10 may be compared with that of the three-phase case, namely

$I_{\text{fault}}=3E_{NA}/(Z_1+Z_2+Z_0+3Z_f)$ in conventional notation (11)

The equivalent-sequence network of the four-phase circuit under consideration is shown in Figure 9. It follows from equations 9 and 10.

If we apply normal phase voltage (line-to-neutral) to this network, the resulting current is one of the sequence currents (all alike in phase *A*); if we apply four times that voltage, the resulting current is the fault current.

Equations 9 and 10 hold equally in absolute values or in per unit values, also in percentage values, in which latter case, however, the fault current is in terms of "times normal."

To illustrate the calculation of the terminal voltages under fault condition, V_{NB} will be seen to be given by equation 12 as the open-circuit voltage minus the four impedance voltages of phase *B*

$$\overrightarrow{V_{NB}}=\overrightarrow{E_{NB}}-(I_1/\overline{90^\circ}Z_1+I_2/\overline{180^\circ}Z_2+ I_3/\overline{270^\circ}Z_3+I_0/\overline{360^\circ}Z_0) \quad (12)$$

LINE-TO-ADJACENT-LINE SHORT CIRCUIT: *A–B*

In the light of Figure 7, if the load is treated as a short circuit, it will be seen that the open-circuit voltage $\overrightarrow{E_{BA}}$ is consumed by the impedance voltage in phase *A* minus the impedance voltage in phase *B*. And as $\overrightarrow{E_{BA}}$ is equal to $\sqrt{2}\ \overrightarrow{E_{NA}}\ /\underline{45^\circ}$, therefore

$$\begin{aligned}\overrightarrow{E_{BA}}=&(I_1Z_1+I_2Z_2+I_3Z_3+I_0Z_0)-\\ &(I_1Z_1/\overline{90^\circ}+I_2Z_2/\overline{180^\circ}+I_3Z_3/\overline{270^\circ}+ I_0Z_0/\overline{360^\circ})\\ =&\sqrt{2}I_1Z_1/\underline{45^\circ}+2I_2Z_2+ \sqrt{2}I_3Z_3/\overline{45^\circ}+0\end{aligned} \quad (13)$$

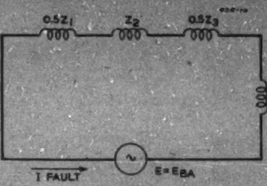

Figure 10. Equivalent sequence network of Figure 7

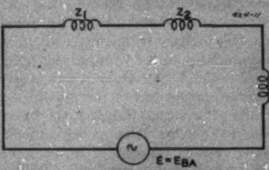

Figure 11. Equivalent sequence network of Figure 7 for static circuits only

If the values of $I_1, \ldots, I_0$ are substituted from equation 6

$$E_{BA}=I_A(0.5Z_1+Z_2+0.5Z_3) \quad (14)$$

Hence

$$I_{\text{fault}}=E_{BA}/(0.5Z_1+Z_2+0.5Z_3) \quad (15a)$$

$$=E_{BA}/(Z_1+Z_2) \text{ for static circuits} \quad (15b)$$

If the fault itself has the impedance Z_f, half of it may be assumed as in series with line *A*, the other half in series with line *B;* and for the sake of symmetry, a $Z_f/2$ may be assumed as existing also in series with each one of the open lines *C* and *D*, so that each one of the four sequence impedances $Z_1, \ldots, Z_0$ is increased by $Z_f/2$, and formulas 15 are transformed to 16.

$$I_{\text{fault}}=E_{BA}/(0.5Z_1+Z_2+0.5Z_3+Z_f) \quad (16a)$$

$$=E_{BA}/(Z_1+Z_2+Z_f) \text{ for static circuits} \quad (16b)$$

Z_f could have been inserted in equation 16 by inspection, without reference to symmetrical components, but the reasoning in the preceding paragraph is given to illustrate a principle and a method.

The sequence network of this is shown in Figure 10 (and 11), except that it

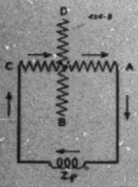

Figure 8. Single-phase load, across opposite lines

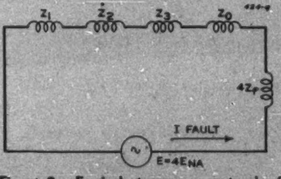

Figure 9. Equivalent sequence network of Figure 6

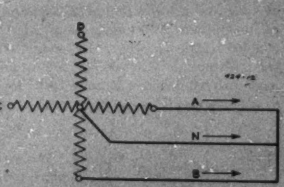

Figure 12. A quadrant short circuit

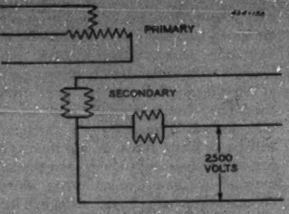

Figure 13A. Three-phase to quarter-phase connected transformer bank

represents only I_f or I_2 properly—I_f if E_{BA} is impressed, I_2 if half that voltage is impressed—but not I_1 or I_0, as a consideration of equation 6 will make clear. Sequence networks may have such limitations.

If the resultant voltages under the fault conditions are required, the same reasoning as in equation 12 applies to the neutral-to-line voltages; and the line-to-line voltages follow from neutral-to-line voltages as in

$$\overrightarrow{V_{CB}}=\overrightarrow{V_{NB}}-\overrightarrow{V_{NC}} \qquad (16c)$$

LINE-TO-OPPOSITE-LINE SHORT CIRCUIT: *A–C*

In the light of Figure 8 and equations 7

$$\begin{aligned}E_{NA}&=I_1Z_1+I_2Z_2+I_3Z_3+I_0Z_0\\&=0.5I_A(Z_1+Z_3) &(17)\\ I_{\text{fault}}&=2E_{NA}/(Z_1+Z_3) &(18a)\\&=E_{NA}/Z_1 \text{ for static circuits} &(18b)\end{aligned}$$

The last is an obvious result, though not equation 18a.

The sequence network is evident from equation 18a.

A QUADRANT SHORT CIRCUIT: *A–B–N*

Case 2, involving lines *A* and *B*, did not involve the neutral, but, in this case, lines *A* and *B* are short-circuited to the neutral (Figure 12). Now there are three different fault currents: I_A, I_B, I_N. As I_N is the (negative) vector sum of I_A and I_B, and as I_A and I_B (as well as I_C and I_D) are the resultants of the sequence currents, the independent unknowns may still be taken as $I_1, \ldots, I_0$, and the equations defining them are

$$\left.\begin{aligned}E_{NA}&=I_1Z_1+I_2Z_2+I_3Z_3+I_0Z_0 &\text{(a)}\\ E_{NB}&=I_1Z_1/\overline{90}^\circ-I_2Z_2+\\&\qquad I_3Z_3/\overline{270}^\circ+I_0Z_0 &\text{(b)}\\ I_C&=0=I_1\sqrt{180}^\circ+\\&\qquad I_2+I_3/\overline{180}^\circ+I_0 &\text{(c)}\\ I_D&=0=I_1\underline{/90}^\circ-I_2+I_3/\overline{90}^\circ+I_0 &\text{(d)}\end{aligned}\right\}\quad(19)$$

These can be solved perhaps more easily numerically, substituting the values of the impedances, the two known currents (0), and the two known voltages (1 or 100 per cent).

The foregoing cases will illustrate how to proceed in the solution of other combination short circuits. Generally, the sum of the known voltages (consumed) and the known currents (zero) will be four, the same as the number of the unknowns, $I_1, \ldots, I_0$, and therefore sufficient to solve the problem.

Example

Each one of two banks of three-phase to two-phase transformers is connected up as in Figure 13a. The secondary (two-phase) windings of each single-phase transformer are seen to be two in parallel, connected up for 2,500 volts. We wish to investigate the characteristics of the system if the two-phase coils are connected up in series for 5,000 volts and arranged four phase (Figure 13b) with the neutral of only one bank (*G*) grounded, the other (*U*) ungrounded.

The impedance data are as shown in Table I. The high value of Z_2 for the transformers arises from the fact that originally no loading of the mid-point of the secondary windings in 5,000-volt connection was contemplated, and therefore no provision was made to interlace the two halves. No values are entered for Z_2 for the primary lines because there is no current in them corresponding to I_2 in the secondary lines. For bank *G*, Z_0 is entered as equal to Z_2, while for bank *U* it is entered as infinity because the neutral of this bank is ungrounded. While, in the design of the transformers, Z_2 and Z_0 are alike, yet in the system as a whole (last column) Z_0 has double the value of Z_2 on account of the particular neutral treatment—one bank neutral-grounded, the other bank neutral-isolated. These figures are all reactance values, the resistance components being ignored here as relatively small.

Currents

The value of the single-phase fault current, from one line to neutral, by equation 9 will be seen to be

$$\begin{aligned}I_{\text{fault}}&=4\times100/(8.8+75+8.8+150)\\&=400/243\\&=1.65 \text{ times normal, based on 15,000 kva}\end{aligned}$$

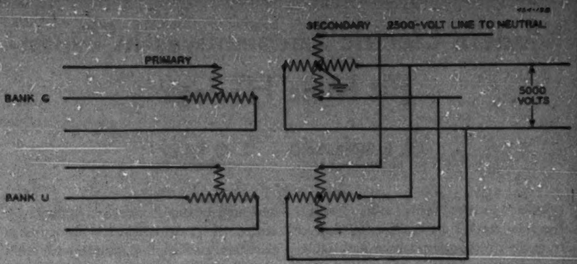

Figure 13B. Two banks of transformers in three-phase to four-phase connection

Bank *G* neutral-grounded; *U* ungrounded

The components I_1, I_2, and I_3 divide equally between the two banks, but I_0 flows exclusively in bank *A* (with grounded neutral). As all components are equal in the fault, the current in the faulty phases (*A*) of the two banks will be

Bank *G*, $I_A=2(5/8)1.65$ times transformer rating
$=2.06$ times transformer rating
Bank *U*, $I_A=2(3/8)1.65$ times transformer rating
$=1.24$

Voltages

The important question about the voltages is particularly the drops through the large Z_2 and Z_0. A view of all four sequence drops is given in Table II.

In item 5 the negative voltage drop of 85.7 per cent means that, under the fault, the voltage of phase *C* rises to 185.7 per cent. Evidently this implies supersaturation of the core leg of phase *C* in bank *G*.

The voltages of the other phases of this or of the other bank can be similarly calculated (remembering that I_0 is zero for bank *U*).

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Modern Electric Locomotives in Open-Pit Mine Haulage

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EARLY in 1942 Phelps Dodge Corporation started production of copper from their new open-pit mine and reduction plant at Morenci, Ariz. One of the earliest mining districts of the state, Morenci had produced some 900,000 tons of copper since the beginning of operations in the early 70's, largely from ores of two to three per cent copper content, mined underground.

The ore body now opened had been explored from the earlier underground workings, and by churn and diamond drilling. It is estimated to contain 230,000,000 tons of ore averaging slightly over one per cent copper, capable of profitable recovery by open-pit mining, concentration, and smelting. The new operation, therefore, will produce more than twice the amount of copper previously taken from the district. In the extraction of this ore, approximately an equal amount of barren capping and slightly mineralized rock throughout the ore body will have to be removed and hauled to lean-ore dumps or to waste dumps in canyons adjacent to the mine.

Preliminary development to uncover sufficient ore and prepare the mine for the planned initial production of 25,000 tons of ore a day was started in 1937. This preparatory development included the removal of some 50,000,000 tons of material, establishment of benches in the mine area, construction of truck haulage roads and standard-gauge railroad for ore and waste haulage. In the first stages this work was handled mainly with 4 $4^1/_2$-yard full-revolving electric shovels and 18 $22^1/_2$-yard end-dump trucks. Later, with the formation of benches and construction of roadbed, rail was laid in the mine, and the main-ore and waste haulage tracks were installed, permitting the work to be taken over largely by trains of 30-yard side-dump cars. Four 125-ton Diesel-electric locomotives with engines of 1,000 horsepower sea-level rating, were put on trial and used in this service, and in the latter stages the 125-ton electric locomotives that had been selected for main-ore haulage were utilized.

In the initial development, 11 mining benches were opened in the ore body, at a uniform height of 50 feet and average width of 100 feet, laid out at even elevations above sea level, the lowest at 4,750 feet and the highest at 5,250 feet. Benches vary in length up to approximately 4,000 feet. Ultimately the pit will be developed to a maximum length of 5,800 feet and will cover 350 acres. The lowest level to be worked will be the 4,200 and the highest the 5,500.

The nearest suitable location for the reduction plant in the rugged terrain of the district was a comparatively level section southeast of the mine. At this site the ore haulage tracks at the primary crusher were established at an elevation of 4,480 feet. From the nearest point of exit from the mine to the crusher the straight-line distance is about 1.8 miles, but the high hills and the town of Morenci lying between these points necessitated a considerably greater length of ore haulage track.

In the early part of the development period, analyses were made of the costs of ore and waste haulage for the planned maximum production of 40,000 tons of ore a day, based on operation on maximum grades of 3.4, 4 or 6.5 per cent. With the decision to limit the maximum grade to four per cent, construction of the track roadbed from the mine to the crusher was started.

The plan of ore and waste haulage tracks at the start of production was approximately as shown by Figure 1; some alterations have since been made. A series of switchbacks on a descending grade of four per cent, compensated for curvature, lead from the mine for nearly two miles to a section of 0.4 per cent descending grade a little less than a mile in length, terminating in a 0.4-mile level-approach track to the primary crusher. Successive mining benches are connected at the ends nearest the ore haulage tracks by switchbacks on four per cent grade. From the opposite ends of the benches tracks on maximum grade of 3.5 per cent lead to approach tracks to four waste dumps in Coronado Canyon and one in Fairbanks Gulch, adjoining the pit. Dump tracks are on 0.25 per cent grade at elevations of 4,800 to 5,200 feet. Something over half of the total waste rock disposal during the life of the mine will be in Coronado Canyon, about 13 per cent in Fairbanks Gulch, and the remainder in Chase Creek Canyon, along the main-ore haulageway. Although the track elevation at the crusher, 4,480 feet, is about midway between the present and the final lowest mining bench, 4,750 feet and 4,200 feet respectively, it is estimated that 85 per cent of all material will be hauled over favorable grades.

Main-ore haulage tracks are laid with 131-pound rail, standard-gauge; bench and waste-dump tracks with 90-pound rail. Double track provided on the main-ore haulage road and passing tracks are not shown in Figure 1.

Comparisons were made of the amount of equipment required and the operating costs for ore haulage with solid-body or side-dump cars of different capacities, and with trains of different numbers of cars. These analyses were based on hauling 25,000 tons of ore in two eight-hour shifts, at the maximum train speeds considered permissible on the several sections of permanent, semipermanent, and temporary track, on an expected maximum average loading capacity of 6,050 tons in eight hours by each $4^1/_2$-cubic-yard electric shovel, and at a maximum dumping rate of 3,400 tons per hour at the primary crusher. Although they indicated a greater investment for equipment and higher energy consumption with side-dump cars in ore haulage, there were compensating advantages in simplification of mine operation and train dispatching by the use of cars of the same type in both ore and waste-haulage service, and in elimination of the large car

Table I. Ore-Haulage Schedule for Locomotives in Continuous Operation Between Mining Bench (4,950 Feet) and Crusher

Number of cars in train............	11
Weight of empty car...............	38.5 tons
Weight of ore load in car............	90 tons
Running time with loaded train, down hill, from end of temporary track on 4,950-foot bench to crusher........	31 minutes
Running time with empty train uphill, from crusher to start of temporary track on 4,950-foot bench..........	24 minutes
Delay of one minute at each of 4 switchbacks in each direction......	8 minutes
Assuming locomotive switches to empty train at crusher and to loaded train at bench:	
Switching time at crusher..........	2 minutes
Switching time at bench...........	2 minutes
Total time of round trip......	67 minutes

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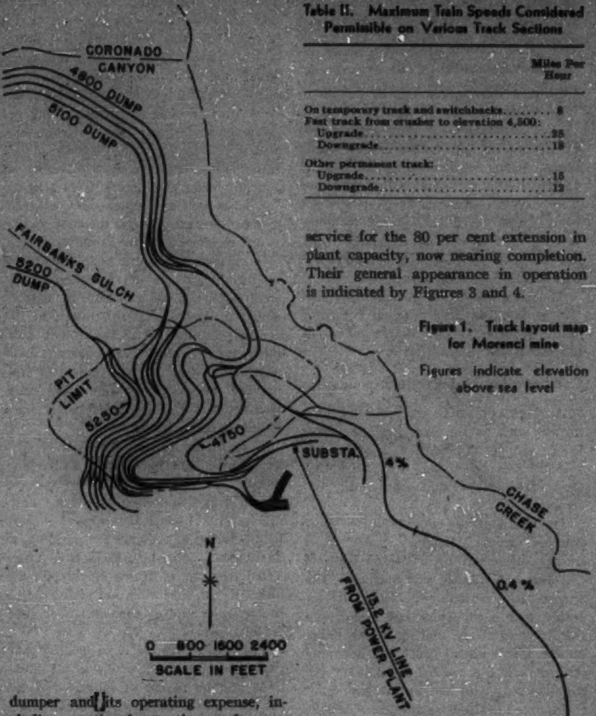


Figure 1. Track layout map for Morenci mine

Figures indicate elevation above sea level

Table II. Maximum Train Speeds Considered Permissible on Various Track Sections

	Miles Per Hour
On temporary track and switchbacks	8
Fast track from crusher to elevation 4,500:	
Upgrade	25
Downgrade	18
Other permanent track:	
Upgrade	15
Downgrade	12

dumper and its operating expense, including spotting locomotives and crews, which would be required to dump solid-body cars at the crusher. The decision was made, therefore, to plan the ore-haulage program on the use of trains of 11 side-dump cars of 40 cubic yards capacity each.

Analyses were made also of the initial investments, operating costs and other features of ore and waste haulage with:

(*a*). Diesel-electric locomotives.

(*b*). Electric locomotives for operation from a 750-volt trolley circuit, using power from the projected 50,000-kw turbine generating plant at the reduction works. Previous estimates on a 1500-volt system had shown no advantage over 750 volts in this service and had confined further considerations to the lower voltage.

Electric Locomotives

In the final analysis, electric locomotives were selected for main-ore and waste haulage at Morenci. Nine locomotives were placed in service during 1941 for the initial production of 25,000 tons of ore a day, and seven more are in service for the 80 per cent extension in plant capacity, now nearing completion. Their general appearance in operation is indicated by Figures 3 and 4.

Operating Voltage

The locomotives are designed for operation at a nominal-contact trolley-line voltage of 750 volts direct current and a maximum voltage of 900 volts at no load in the mercury-arc-rectifier substations supplying the trolley circuit.

Locomotive Weight and Haulage Capacity

The locomotive weight of 125 tons was selected to permit hauling the maximum train of 11 38.5-ton empty cars up the four per cent grades with an estimated adhesion of slightly over 19 per cent, and to provide by electric braking over 40 per cent of the retarding effort required with trains of 11 128.5-ton loaded cars descending the four per cent grades. The cars in use have an empty weight of approximately 45 tons and carry from 90 to 100 tons of ore or waste.

To insure adequate capacity for the most severe duty that might be imposed in haulage service co-ordinated with the maximum rates of loading by $4^1/_2$-cubic yard electric shovels and dumping at the primary crusher, the locomotives are capable of maintaining continuous operation between the mining bench at 4,950 feet elevation and the crusher on the ore-haulage schedule shown in Table I.

The maximum train speeds considered permissible on various track sections are shown in Table II.

Bench Operation

Although it is practicable to operate electric locomotives from temporary trolley structures on the mining benches, in order to free operations of any impediment from such structures in the mining areas and to avoid the delay and expense of maintaining and moving them in the progress of mining, it was deemed advisable to make other provision for train movements on bench track. Alternative arrangements considered were:

(*a*). Cable reel on the locomotive for power supply on bench tracks beyond the ends of electrified sections. Such operation was somewhat dubious because of the great lengths to which the mine benches will extend, the slow speeds necessary when operating on cable reel paid out from the locomotive, and the probability of rapid wear and short life of cable in such service.

(*b*). Auxiliary Diesel-engine–generator set on the locomotive to supply power for train movements on unelectrified bench tracks. Because of the increased cost of an auxiliary unit of sufficient power for satisfactory movement of loaded and empty trains on bench tracks, and the operating complications which would be introduced, this arrangement was concluded to be undesirable.

(*c*). A storage battery on the locomotive of sufficient capacity for train movements at the desired speeds on the benches was decided to be worthy of trial.

Each locomotive is equipped with a storage battery applied for an expected life of approximately five years. The most severe duty cycle on the battery for train movements on benches was estimated to be for power supply in the following operations:

Hauling a train of 11 empty cars from the start of temporary track on the 4,950 foot bench to the far end, a distance of 4,000 feet, in approximately $5^1/_2$ minutes.

Spotting 11 cars at the shovel for loading at the rate of 15.75 tons per minute.

Accelerating the train of 11 loaded cars at a maximum rate of 0.1 mile per hour per second and hauling it back to the trolley at the start of permanent track in approximately $6^{1}/_{2}$ minutes.

Operating air compressor for braking service, and supplying lighting, heating, and control circuits.

The 240-cell 500-volt lead-acid storage battery applied to each locomotive has a discharge capacity of 500 ampere-hours on a six-hour basis, approximately 70 per cent of the estimated output required for six complete bench cycles in average duty. The complete battery weighs approximately 14 tons.

A large part of the battery output in each bench cycle is restored during the succeeding interval of time in which the locomotive is operating under the trolley to the crusher with loaded train, while dumping cars and when returning the empty train to a mine bench. Automatic charging equipment on the locomotive connects the battery to the trolley circuit through suitable resistors, providing at the start a high charging rate and later a reduced rate when the battery charge has been restored to a predetermined value.

With the locomotives in service for three eight-hour shifts each day, the batteries automatically are returned to full charge during the runs to and from the crusher. Under other conditions, such as waste-haulage and dump operation, occasional lay-up for recharging is necessary. Periodic equalizing charging is also regular practice. A lay-up track is provided at the mine pit shops for recharging from the trolley wire, and the batteries automatically are disconnected at full charge. Daily gravity readings are taken to check battery conditions.

As it is expected that, after some years of further development of the mine, the frequency of movement of bench tracks will be less than that necessary in present operation, and it later may become advantageous to operate from portable trolley over the bench tracks, all locomotives are arranged for the addition of side-arm collectors, when desired.

ELECTRIC BRAKING

Studies were made of the merits of regenerative braking or dynamic braking on the locomotives to assist the straight or automatic air brakes in retarding loaded trains on descending grades, and thereby reduce wheel and brake-shoe wear and maintenance and replacement costs. The decision to adopt dynamic braking was based mainly on the lower cost and relative simplicity of locomotive and substation equipment.

The dynamic brake on each locomotive is capable of producing retarding efforts up to 50,000 pounds at train speeds between 8 and 18 miles per hour.

Construction of Locomotives

Haulage conditions encountered in a project such as the Morenci mine have led to the development of a locomotive specially adapted to this duty. Figure 5 shows the general dimensions and construction.

The running gear consists of two four-wheeled trucks, having welded steel frames supported by a combination of coil and half elliptic side springs. The trucks are permanently coupled together with a mallet or ball hinge, and the drawheads are built into the truck bumpers, so that all draft and bumping stresses are continuous throughout the running gear and are not in any way transmitted to the cab structure. Articulated trucks are particularly suited to the severe duty on rough track, grades, and curves, and do not swing crosswise to the track in case of derailments.

The cab has a rigid underframe which is supported on two center pins located in the middle of the truck bolsters. Side-spring restraining plungers carry a portion of the cab weight on the inner truck bumpers. One center pin is provided with longitudinal motion to compensate for the changing center distances on curves. Normal design for a locomotive of this length and type would weigh considerably less than 125 tons. The additional ballast required by the starting conditions has been built into the structural members and provides a large margin of mechanical strength. A novel structural feature is the use of heavy end plates and rock guards which protect the motors, journal boxes, and steps from damage by loose rock and other track obstructions. Clearance below the rock guards is one inch less than the motor clearance.

To provide maximum visibility for the operator, cabs are of the steeple type, with sloping hoods at each end, and an elevated operating compartment in the center. In addition to the control compartment, there are five apparatus compartments, two at the ends for the battery, two adjacent equipment compartments, and a central resistor space under the operating deck.

Each of the two equipment compartments includes a blower for two traction motors and an air compressor, one for the air brakes and one for the car-dumping system. Electrical control apparatus for operation of the traction motors and auxiliaries is also housed in this space. Ventilating air is drawn in through air filters in the side walls and delivered to the traction motors through the hollow center bearings.

The control station is so located that the operator has full vision in all directions and by means of mirrors can see the blind side of the train. A double-shoe pantograph trolley is mounted on the roof of the operating compartment.

PERFORMANCE

Under present operating conditions, a typical round-trip run between loading benches and the crusher is nine miles, with 470 feet change in elevation and

Figure 2. General view of the mine pit and benches, showing progress made early in 1943

Figure 3. Electric locomotive spotting ore cars for loading at a bench shovel

Figure 4. Locomotive and train unloading waste

switchbacks involving four stops and reversals in each direction. The maximum round-trip run is 15 miles, with 770 feet change in elevation and several additional switchbacks. Numerous four per cent grades averaging 1,300 feet in length on switchbacks are with the load, which means that upgrade operation with the empty train is an important factor in determining locomotive capacity and schedule speeds.

The average round trip from bench to crusher requires about one hour, to which must be added a 1- to $1^1/_2$-hour loading period on the bench. Under ideal scheduling of operation, nine round trips will move 7,000 to 8,000 tons of ore in 24 hours.

Train lengths are adapted to the length of tail tracks at the switchbacks. Normally, trains are made up of nine 45-ton dump cars, each of which will hold 90 to 100 tons of ore, making a trailing load of 405 tons upgrade, and 1,200–1,300 tons downgrade. Safe downgrade speeds with full load are limited by curves and track conditions to from 12 to 18 miles per hour. Return speeds with empties are from 15 to 25 miles per hour, depending on the grade.

Speed and tractive-effort characteristics of the locomotive are shown in curve data form by Figure 6.

Motor Control

Because of the great variety of starting and running conditions, the three-speed system of motor control is used, involving series, series-parallel, and parallel connections for the four motors. The one-quarter and one-half speed connections are particularly useful in battery operation where economy of ampere-hours is important. Spotting of cars at the shovels and crusher also makes use of the low-speed notches.

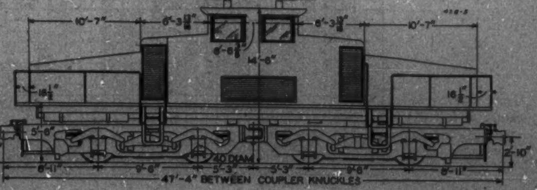


Figure 5. Side view and general dimensions of 125-ton electric locomotive

There are a total of 22 starting notches, an ample number for starting a loaded train on grades and curves, without wheel slippage. Sand is used when rail conditions are abnormal. All motor combinations and resistor switching are managed with remotely controlled electropneumatic switches. The motors are also switched from line to battery power with an electropneumatic transfer switch. The controls for all motor and auxiliary operations are centrally located so that the operator has full charge of the entire locomotive without moving from the control platform.

Figure 7 shows the distribution of the starting notches in terms of speed and load, and also the dynamic-braking notches which control retardation. Maximum current for acceleration can be held at 500 amperes per motor, and for braking 400 amperes per motor. Motor currents are indicated on cab ammeters at all times.

In the interest of simplicity and economy of space, the traction motor switching and resistor equipment are arranged for the double purpose of starting and dynamic braking. The additional equipment necessary for braking is thus held to a minimum.

Figures 8, 9, 10, and 11 illustrate the switching circuit combinations used for series, series-parallel, parallel, and dynamic braking.

As the master controller is moved from the 8th to the 9th, and from the 15th to the 16th notches, the motors are reconnected in the different combinations, resulting in higher voltage per motor and higher speed. The process of reconnecting the motors is known as transition. In the ninth notch, the motors of each truck are in series but the two trucks are now in parallel, giving one-half voltage to each motor and its series resistance. During the interim between the eighth and ninth notches, the motors of the number 2 truck are by-passed, giving a momentary loss of tractive effort. This is known as shunting transition.

Between the 15th and 16th notches, the motors in the two trucks are reconnected in parallel, giving full voltage per motor circuit. The change in this case is made by the bridge method wherein the transition is set up by paralleling the two motor circuits without interrupting the flow of current in either motor and, consequently, without loss of tractive effort.

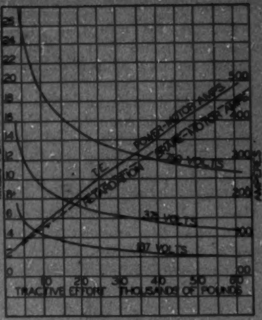


Figure 6. Locomotive performance and tractive-effort curve

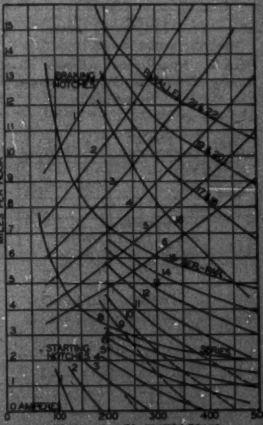


Figure 7. Power and brake-notching curves showing uniformity of current regulation

BRAKING

Obviously, the control of braking speeds with loaded trains on four per cent downgrades is an important feature of safe operation. Three braking systems are provided, the standard automatic air brake, a separate straight air nonautomatic train brake, and the dynamic brake.

The total braking effort necessary to hold a loaded train on four per cent grade is approximately 100,000 pounds which, if developed entirely by the brake shoes, would result in overheating and rapid wear of shoes and wheels, with frequent replacements and excess maintenance.

The dynamic brake, which is a generating action developed by the traction motors, is designed to supply approximately one half of the total retardation needed, or about 50,000 pounds. This relief of brake-shoe duty more than doubles the life of brake shoes, reduces the frequency of flat wheels, and provides an important safety factor in the handling of trains.

Series traction motors can be made to act as generators, and, when so doing, their torque is reversed, and they produce a braking action. This action, known as dynamic braking, is used to supplement the air braking to hold trains at constant speed on downgrades. The electrical output of the generators is absorbed by resistors.

If a series motor is driven from some mechanical source of power (as a locomotive axle) and a resistor is connected across its terminals, it will not build up to act as a generator because of improper direction of current in the series field. However, by using a pair of motors and connecting the resistor from a point between the armature and field of one motor to a corresponding point of the other motor, as shown by the circuit through the *BR* switch on the main schematic, the armature of one motor feeds the field of the other, and braking is obtained.

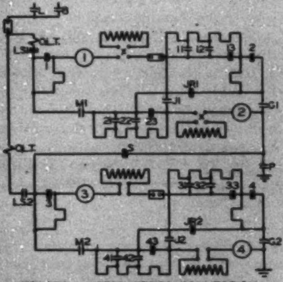
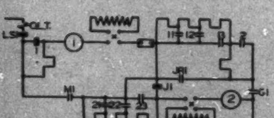
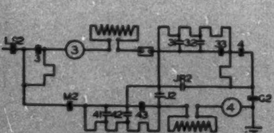


Figure 8. Motor-control schematic diagram showing series connection for slow-speed operation

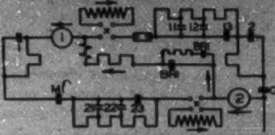


SERIES-PARALLEL CONNECTIONS SAME FOR 3&4

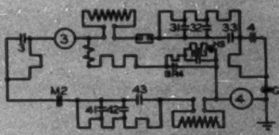


PARALLEL CONNECTIONS SAME FOR 1&2

Figure 9. Schematic diagram for series-parallel and parallel connections



BRAKING CONNECTIONS SAME FOR 3&4



COASTING CONNECTIONS

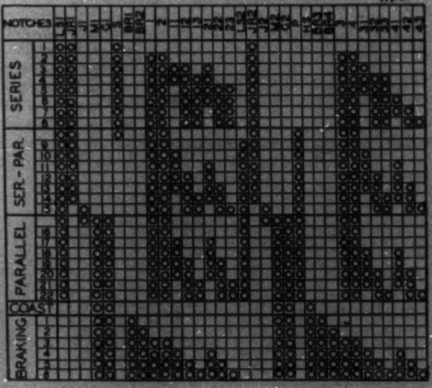
Figure 10. Schematic diagram of coasting and dynamic-braking connections

Each pair of motors, therefore, acts as a dynamic-braking system feeding its output into the resistor through the *BR* switches. Advance of the braking control varies the amount of resistance in series with each motor and hence the speed at which full dynamic-braking effect is obtained.

A resistor is connected across the *BR* 3 switch and is brought in circuit by the *HS* contactor, which is closed whenever the controller is in the "off" position. Its purpose is to set up a high-resistance braking circuit during coasting. The amount of braking effort produced by this circuit is negligible, but the current flowing in the resistance serves to provide a voltage for actuating the overspeed relay. At speeds above approximately 18 miles per hour, the voltage across the resistance is sufficient to cause the relay to lift, and this prevents the establishment of the full braking circuits. It is, therefore, impossible to obtain dynamic braking at excessive speeds, which would generate abnormally high voltages.

The energy generated by retardation must be dissipated as heat in the resistors. This is also true of the starting losses. In the service described, all of the resistor load periods are of short duration, making an ideal application for grid resistors of large thermal capacity, with natural ventilation. A small amount of air is diverted from the motor blower system to supplement ventilation of the resistors and to continue the cooling process while the locomotive is standing.

To get the maximum benefits from the dynamic system in holding a constant speed on heavy grades, there must be facilities for building the dynamic retardation up to a predetermined amount and then supplementing this with suffi-


NOTCHES
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PARALLEL
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Figure 11. Sequence of switching applying to diagrams shown in Figures 8, 9, and 10

Figure 12. General view of a 750-volt 375-horsepower traction motor

Figure 13. Operator's station, showing the arrangement of control, brake, and auxiliary levers

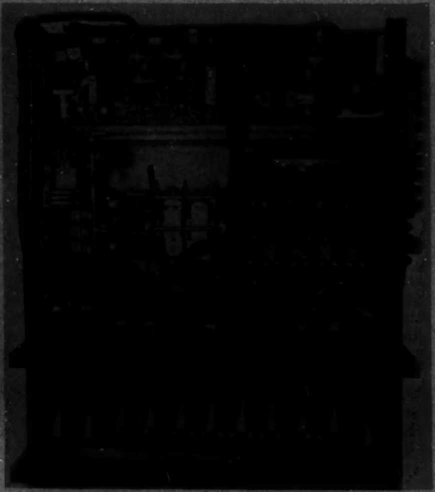

Figure 14 (right). Preassembled panel of motor-control apparatus

cient air braking to suit the load, grade, and the desired speed. In order to simplify the procedure of blending the two brakes, the dynamic load is regulated by series resistors to a constant value. Current-limit relays control the closing and opening of resistor switches to hold the braking effort between narrow limits over a speed range between 18 and 8 miles per hour. After the initial application, this regulation is automatic, leaving the operator free to give full attention to the manual graduation of the straight air train brake as needed to hold the desired speed.

MOTORS

Four 750-volt 340-horsepower axle-mounted traction motors drive the four axles through single-reduction spur gearing. Mechanical and electrical design features are especially suited to the type of service encountered in mine haulage.

A general view of the traction motor appears in Figure 12, which shows the frame as a heavy steel casting, with roller-type armature bearings and oil and waste axle bearings. Labyrinths on the armature shaft and housing retain the grease for the roller bearings, so that greasing is needed only at infrequent intervals. The air intake for forced ventilation is cast into the frame at the rear and obtains air from a vertical opening in the truck bolster.

The inherent inductance of the motor is sufficient to smooth out ripples in the current supply. In addition, laminated commutating poles are used to reduce sparking at the commutator with sudden changes of current due to interruption and reapplication of power.

Armature and field coils are finished with class-*B* insulation, and the armature coils are heated and pressed before installation. After being connected, they are banded while hot, wedged with Micarta, and rebanded with nonmagnetic banding wire. These features reduce operating losses, improve the continuous rating, and reduce sparking volts at the commutator. Field coils are seated between machined pads in the frame and spring washers under the pole tip. Coils are fitted tightly on the poles.

Commutating characteristics are such as to withstand the over-voltages encountered in dynamic-braking service and hold the volts per bar on the commutator within conservative limits. Armatures are dynamically balanced both before and after winding.

Similar operating margins have been incorporated in the design of the blower and compressor motors. Each blower motor is rated ten horsepower at 400 volts and 2,100 rpm. The two are connected permanently in series, to take advantage of the resulting low-voltage commutator design. Compressors use direct drive from 25-horsepower, 800-rpm motors which are wound for full

Figure 15. A typical 1,000-horsepower 125-ton Diesel-electric locomotive

voltage and very generously proportioned to withstand the frequent starting service.

Operating Equipment

In arranging the operator's cab, particular attention has been given to convenience and visibility needed for frequent reversals of train movement. Figure 13 shows the arrangement which has proved to be most satisfactory.

All normal starting and braking functions are controlled by means of conveniently located master control and brake handles as follows:

- **22-notch motor controller.**
- **Reversing-control lever.**
- **Motor cutout switch.**
- **Dynamic-brake master controller.**
- **Automatic air-brake valve.**
- **Independent locomotive brake lever.**
- **Straight air train-brake valve.**
- **Disconnect switch for auxiliaries.**
- **Miscellaneous auxiliary switches and push buttons.**

Certain of the auxiliaries, such as lights and master-control supply are fed directly from the battery at all times. Other auxiliaries, such as blowers and compressors, are switched to line or battery by the main transfer switch which is thrown by the operator when passing to and from the trolley-wire sections.

For convenience in maintenance, as well as in original manufacture, the motor-control equipment has been mounted in preassembled racks, as shown in Figure 14. One of these assemblies is mounted in each of the two hood compartments, which are isolated completely from the operator's cab. All power-switching equipment is remotely controlled and is of the well-known electropneumatic type, particularly adapted to heavy duty. An important factor in service reliability is the use of 1,500-volt rated insulation for power apparatus.

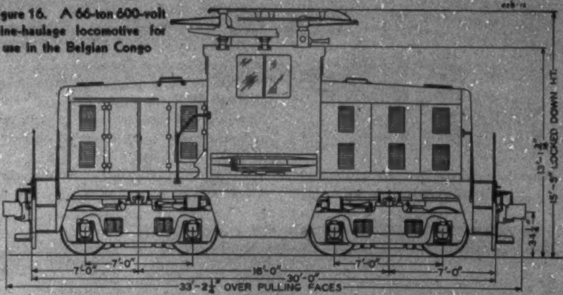

Figure 16. A 66-ton 600-volt mine-haulage locomotive for use in the Belgian Congo

In constructing the second lot of Morenci locomotives, it has been possible to incorporate numerous details of arrangement for operating and maintenance convenience, based on experience in handling the original installation. The final design is, therefore, adapted to the operating conditions on this particular property and also establishes standards which can be applied universally wherever large quantities of material are to be handled. That these aims have been realized, is indicated by the high degree of locomotive availability which is being maintained at Morenci Mine.

Other Modern Haulage Locomotives

Figure 15 illustrates one of the 125-ton Diesel-electric locomotives used in waste haulage and for general duty at Morenci Mine. With skillful dispatching, it is practicable to make economical use of both types of motive power in the varied phases of haulage service.

Stripping operations at present require the removal of approximately equal amounts of ore and waste. The Diesel-electric locomotives of 125 tons are used for a portion of the operation on the long dump tracks, which have light grades and no overhead wire. Line construction is being extended, with the addition of substation capacity, all pointing to the further general use of the electric locomotives.

The electric locomotive illustrated in Figure 16 is one of five 66-ton 42-inch gauge 600-volt units now under construction for use in open-pit copper mining in the Belgian Congo. The general characteristics are quite similar to those of the Morenci locomotives, with the exception that, instead of a storage battery for bench operation, a cable real and side-arm collectors are to be used. Short benches, and other differences in the mine layout indicate that a battery is not essential to satisfactory performance.

Maximum grades on this property are two per cent against the load. The empty train amounts to a trailing load of 220 tons, and the loaded train is 440 tons. These locomotives will handle all of the ore and waste haulage and will be subject to diversified grade conditions.

A New Electric Hoist Drive for Cranes

M. A. WHITING
ASSOCIATE AIEE

A NEW system of electric hoist drive has been developed, and its success has been demonstrated in practical use. For the types of hoist for which it is particularly intended this system provides a closer approach to the ideal characteristics than has previously been available. Although the system is not intended for use in all of the important types of hoist, it is expected that further experience and additional study of details and refinements will broaden its field of application beyond that which has, to date, been established in practice.

The Ideal Characteristics

Among the several hoisting applications for which the new system is believed to be suitable, the high-grade heavy-duty indoor cranes in steel mills and heavy machine shops may be considered typical. A study of the requirements indicates that the ideal characteristics for a crane of this type and of certain other types are principally as follows:

1. The same manipulation of the controller to any position in the lowering direction should cause the motor either to deliver power for lowering an empty hook or to absorb power for the proper lowering of a load, whichever is required by the conditions at that moment.
2. Maximum hoisting speed at empty hook should be a definite value, approximately twice the speed at rated load.
3. At the full-speed hoisting position of the controller the stalled torque should be limited to a reasonable overload value.
4. At the first-speed hoisting position the speed-torque curve should be relatively flat, having a very low speed at empty hook but a substantial stalled torque.
5. The maximum lowering speed at empty hook should be a definite selected value, between 160 per cent and twice rated hoisting speed.
6. Maximum speed of lowering rated load should be substantially less than at empty hook, and maximum speed of lowering maximum load should be less than that of lowering rated load.
7. The minimum lowering speeds, that is, the speeds at the first lowering-speed position of the controller should be nearly alike, irrespective of load. If readily attainable, it is preferable that this lowering speed be *less* at heavy loads than at partial loads or empty hook.
8. The electric equipment must have in the lowering direction a substantial margin, so that, when the maximum load which can be lifted is handled, the torque capacity available in the lowering direction is ample to maintain dependable control of the load under the most adverse conditions.
9. A safe and reasonably smooth retardation and stop must occur automatically in the event of failure of incoming power and other emergencies.
10. The regulating means by which the fifth, sixth, and seventh requirements are met must *not* be capable of stalling a load which is being lowered, however great that load may be, at any lowering position of the controller.
11. The electric equipment should be self-protective against abuse, either of itself or of the mechanical equipment, under the condition of rapid and unrestrained movement of the controller handle.
12. It is usually important that the holding brake be so controlled automatically that in regular service it is not required to supply any large part of the retardation effort.

The reasons for some of the items in this list of ideal requirement are probably evident. The reasons for some of the other items have been explained by J. A. Jackson.[1]

Characteristics of Previous Systems

The most highly developed crane-hoist system using an a-c motor consists of a conventional wound-rotor induction motor and secondary resistance control with the addition of d-c dynamic braking in the lowering direction. The d-c dynamic braking is obtained by exciting the motor stator with direct current and absorbing in a controlled resistor the power regenerated by the rotor when lowering a load. This system has proved very useful, particularly in shipyard cranes. Figure 1 shows typical speed curves at the various controller positions plotted against load on the hook, at an assumed mechanical efficiency of 87 per cent. Certain additional speed-load characteristics, not shown in Figure 1, are provided solely as automatic transition points for acceleration and retardation. The examination of Figure 1 shows that this system fulfills some of the ideal requirements stated in the introduction but departs considerably from this ideal in certain other respects.

For use with d-c power, the most highly developed crane-hoist-motor system commonly used is one in which the hoist motor is built as a series motor and is operated as a series motor in the hoisting direction. In the lowering direction a resistor of substantial capacity is connected to furnish a component of separate excitation to the series-wound field and to provide a dynamic-braking path for absorbing current regenerated by the armature when a load is being lowered. Several manufacturers build equipments of this type which are essentially similar but differ in some details, and whose characteristics are similar, with minor differences. This system has been in general and successful use for approximately 30 years and is a radical improvement over earlier practice. A typical set of speed-load characteristics is shown by Figure 2, which is a combination of Figures 5 and 6 of Jackson's paper.[1] A study of this figure shows that it meets or approximates some of the ideal characteristics set forth in the introduction but departs widely from this ideal in certain other respects.

That form of the Ward-Leonard system which uses a three-field "shovel-type" generator[2,3] has occasionally been proposed for the type of hoist here considered. Although this has been the standard system for shovels and is an

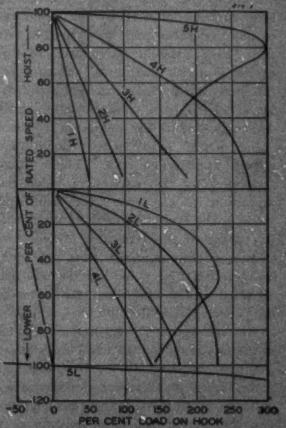

Figure 1. Characteristics of a-c crane hoist motor with d-c dynamic braking

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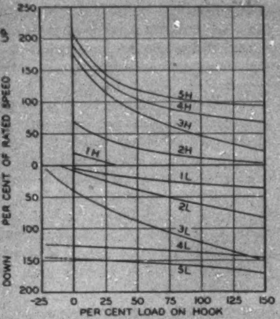

Figure 2. Characteristics of d-c series crane hoist motor with dynamic braking

excellent choice for certain other applications, its speed-torque curves differ widely from most of the ideal characteristics here considered.

The New System

The ideal characteristics for the applications under consideration have been approximated closely by the development of a radical improvement of the Ward-Leonard system, the principal feature of which is the addition of an exciter of unusual design embodying a cross-flux principle. By this means, a characteristic is obtained in which the voltage of this cross-flux exciter is responsive in a unique manner to the variations of magnitude and polarity of the current in the "loop" circuit, that is, the local circuit comprised by the armatures of the generator and hoist motor. At zero "loop" current the voltage of this exciter is at its maximum. At substantial increases of "loop" current, *irrespective of polarity*, the voltage of this exciter decreases, and at the maximum overload values of the "loop" current the voltage closely approaches zero. The voltage generated by this exciter provides the excitation of the generator field and modifies the excitation of the motor field. By means of the generator and motor-excitation characteristics thus provided, characteristics of speed versus load are obtained of the kind illustrated later in the paper (Figures 6 to 9).

The 9th and 12th requirements listed in the foregoing for the ideal crane hoist are met respectively by a simplified arrangement of an emergency dynamic-braking resistor in the hoist-motor armature circuit and by an improvement in the control details of the magnet brake. These two features are not dependent upon the cross-flux-exciter principle. They were, however, developed as contributions to the same project, namely, the attempt to achieve the almost perfect crane-hoist drive. Because of lack of space, these features are not described.

Construction of the Exciter

Figure 3 illustrates diagrammatically the principles of construction of the cross-flux exciter in its basic form. Although multipolar machines are possible, the exciters built to date are bipolar, by which is meant that the armature-winding and commutator connections are those of a normal bipolar machine. Four pole pieces are provided. In Figure 3 the two upper pole pieces constitute one "pole" insofar as relates to the generation of the output voltage. The two lower pole pieces constitute the other "pole." For reasons which will appear, a degree of artificial saturation is introduced in the pole pieces, for example, by notches in the sides of the pole pieces as indicated in Figure 3. In one design which has been used, all pole pieces are duplicate (ordinary manufacturing variations being entirely acceptable). In another design which has also been used, the diametrically opposite pole pieces *P*1 are duplicates, each having a high degree of artificial saturation; the diametrically opposite pole pieces *P*2 are duplicates but have a much less degree of artificial saturation. For the general explanation, let all pole pieces and their respective air gaps be considered duplicate.

Each pole piece carries a shunt main-field winding separately excited from the constant-voltage main-excitation bus, also a cross-field winding consisting of a few turns of heavy conductor connected in series in the "loop" circuit, that is, in the main armature circuit of the Ward-Leonard system. Corresponding windings ordinarily have equal numbers of turns upon all pole pieces. The polarity axis of the separately excited main field is shown by arrow *m*–*m* in Figure 3. Stated in another way, when the sole excitation is by this main field the two upper pole pieces are of like polarity, for example "north," and the two lower pole pieces are of like polarity, "south." The polarity axis of the heavy cross field is in quadrature to that of the main field, that is to say, if the sole excitation were that of the cross field, the two right-hand pole pieces would be of the same polarity, for example "north" at the normal direction of "loop" current, and the two left-hand pole pieces correspondingly would be of "south" polarity. For the normal direction of "loop" current, the polarity axis of the cross field is shown by arrow *C*–*C*. As a result of this arrangement of coil polarities, at the normal direction of "loop" current, the respective field windings are of additive effect upon both pole pieces *P*1 but are opposed upon both pole pieces *P*2, as indicated by the small arrows located alongside the respective coils in Figure 3. The respective fluxes, although merged in parts of the magnet frame and armature core, are separate in the pole pieces, air gaps, and armature teeth, in which almost all of the reluctance occurs; hence for all practical purposes it is substantially correct to consider the machine as having two independently saturable flux paths, one along line *P*1–*P*1, and the other along line *P*2–*P*2.

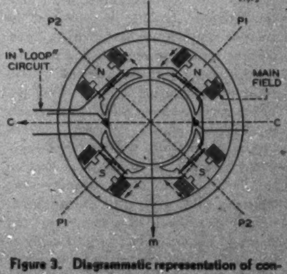

Figure 3. Diagrammatic representation of construction of cross-flux exciter

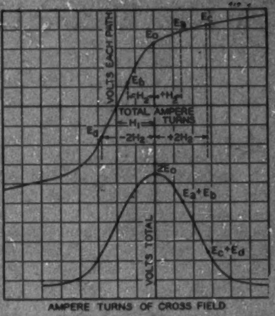

Figure 4. Theoretical characteristic of cross-flux exciter

To avoid a possible misunderstanding, it should be noted that in Figure 3 the positions of the brushes as shown are diagrammatic only and represent the positions of the armature slot conductors at which they undergo commutation.

Commutation does not present any

greater problem than in an ordinary machine. Commutating poles are provided in accordance with usual practice. The commutation and characteristics are not sensitive to brush position, brush fit, or ordinary manufacturing variations.

Characteristics of the Exciter

In the circuit arrangement in which this exciter is used in the system, armature reaction and armature *IR* drop of the exciter tend, if not offset, to be of more than negligible effect, but they can be compensated sufficiently to make them almost negligible; consequently the explanation of the operation of the cross-flux exciter can be based upon a study of the no-load saturation curves of the respective flux paths. The upper part of Figure 4 represents the no-load saturation curve, not of the entire machine but of each of the two duplicate and practically independent flux paths, *P*1 and *P*2, respectively.

*H*1 represents the selected field strength of the separately excited main-field winding which remains substantially constant throughout. When the current in the cross field is zero, each flux path of the exciter causes the generation of a voltage E_o, and the total voltage generated is $2E_o$ plotted in the lower part of Figure 4 at zero cross-field ampere turns. Now assume a cross-field strength of *H*2. In flux path *P*1 the value of *H*2 is additive to *H*1, hence the voltage generated in this flux path is now E_a. In flux path *P*2 the value of *H*2 is subtractive from *H*1; hence the voltage generated in this flux path is now E_b. The total voltage generated is E_a+E_b, as plotted in the lower part of Figure 4. Since saturation increases beyond point E_o and decreases below E_o, voltage E_a+E_b is substantially less than $2E_o$. At a cross-field strength of 2*H*2 the further increment of voltage generated in path *P*1 is very small, but the decrement of voltage generated in path *P*2 is large—in fact the voltage of path *P*2 reverses. The corresponding total voltage E_c+E_d accordingly is much reduced. When the two magnetic circuits are duplicate, and when the numbers of turns of the respective windings are equal upon all pole pieces, the negative voltage generated by path *P*2 cannot become quite so great as the voltage generated by path *P*1; hence the total voltage of the exciter never reaches zero. When the polarity of the cross-field current is opposite to the foregoing, path *P*2 behaves as did path *P*1 in the foregoing and conversely. Hence the characteristic of voltage generated versus cross-field (that is "loop" circuit) current tends to repeat symmetrically about the left-hand side of the vertical axis in the lower part of Figure 4. If compensation for armature reaction is not provided, this characteristic shows a departure from symmetry about the two sides of the vertical axis. The dissymmetry caused by armature reaction may or may not be practically disadvantageous, according to the specific design and application. To date, a design of exciter having closely symmetrical characteristics about the vertical axis has not been built. In the first design the armature reaction was not compensated. In a later design of a larger exciter the armature reaction was compensated, but a dissymmetry of characteristics was purposely introduced by means of unlike proportions of the respective flux paths.

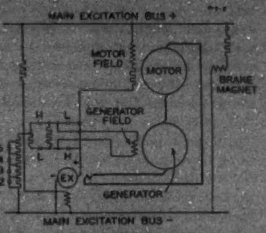

Figure 5. Elementary diagram—circuit arrangement of Ward-Leonard system using cross-flux exciter

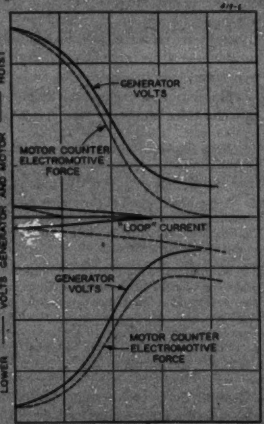

Figure 6. Theoretical characteristics—generator voltage and motor counter electromotive force of system of Figure 5

If a moderately greater value of separately excited field is held, represented by a higher value of H_1 in Figure 4, a voltage characteristic is obtained of similar shape having higher voltages throughout, and conversely for a moderately smaller value of separately excited field.

If the exciter of Figures 3 and 4 were only slightly saturated in each of its duplicate flux paths at the maximum cross-field current, its minimum output voltage (although substantially less than maximum) would not be so low as desired; hence a high degree of saturation is provided. This is done most suitably by providing a restricted section in each pole piece, for example by notches which are deep and also relatively long radially.

For reasons which will appear in the explanation of the system as a whole, it is usually important to offset or minimize the effect of *IR* drop of the exciter armature. Although not strictly necessary, it is usually advantageous to compensate the exciter for its armature reaction. By itself compounding may be provided by turns about each pole piece connected in series with the exciter armature, having polarities coinciding with that of main axis *m–m*. Perfect compensation for armature reaction tends to require a winding distributed over the pole arcs. Practically, however, a concentrated compensation provides all the benefit which is important. Such compensation can be provided by turns about each pole piece connected in series with the exciter armature, having polarities in line with the cross axis *C–C*. It might seem that for both of these improvements two field windings must be added to those of Figure 3. Actually only one added winding is required. Upon two pole pieces the polarities of the individual compounding and compensating windings would be alike, hence only one winding per pole piece is added having the sum of the turns individually required. Upon the other two pole pieces the polarities of the individual compounding and compensating windings would be opposed; hence only one winding per pole piece is added having the polarity and number of turns corresponding to the difference of the individual requirements.

Design and Arrangement of System

The generator is of normal design. Its main field is wound with the maximum practicable cross section of copper in order to provide the desired high no-load voltage with the least oversize of generator. Such a field winding provides a generator time constant which, in combination with

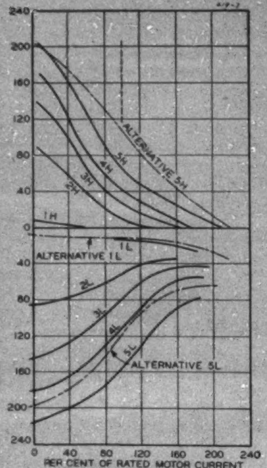

Figure 7. Characteristics from tests of developmental sample equipment

the circuit arrangement and cross-flux-exciter characteristics, limits the peak currents to values which are suitable for the electric equipment and the hoist system.

The generator, cross-flux exciter, and an ordinary constant-voltage exciter are driven by an induction motor, or if preferred for a sufficient reason by a synchronous motor or a d-c motor. An engine drive tends to be an improper choice, since the engine is unable to reconvert the energy regenerated by a descending load and hence is unable to restrain the speed of a descending load.

The hoist motor has a nonstandard main-field winding for a variable separate excitation but in other respects is of the type which is standard for high-class crane-hoist installations.

If it were intended to use the cross-flux exciter solely to furnish a generator excitation of the desired characteristics, it would be appropriate to consider an arrangement which connected the generator field directly to the armature of the cross-flux exciter, whereupon the hoist could be reversed and controlled by reversing and controlling the strength of the separately excited main field of this exciter. However, another important function of the cross-flux exciter is to provide an advantageous variation of the hoist-motor excitation. In order that one cross-flux exciter shall serve both of these purposes (by virtue of the unusual circuit arrangement used), it is necessary that the main-field excitation of this exciter be maintained nonreversible and at a substantial value which is not far from constant. Accordingly, a conventional reversing control is provided between the exciter armature and the generator field, as indicated in the elementary diagram of Figure 5.

In order to provide the desired variation of the hoist-motor field, the cross-flux exciter is designed for a maximum voltage substantially less than that of the constant-voltage main-excitation bus. One terminal of the cross-flux exciter, negative as shown in Figure 5, is connected permanently to the main-excitation bus terminal of like (that is negative) polarity. Thus between the positive terminals of these two sources of excitation a variable voltage is available which is the *difference* of the two voltages. The hoist-motor field is connected permanently between these two terminals. The excitation voltage thus made available may be visualized by referring to Figure 4 and considering the ordinate between the lower and the upper base lines as the main-excitation bus voltage. Thereupon the height of ordinate from the curve "volts total" upward to the upper base line represents the voltage applied to the hoist-motor field. Thus the hoist-motor field is weakest when the "loop" current is substantially zero, which occurs when the empty hook is being hoisted and lowered and when the motor is at standstill at the "off" position of the controller. As the "loop" current increases, the decrease of the cross-flux-exciter voltage causes an increase in the motor-field strength. Normal field strength occurs at approximately rated armature current of the hoist motor. Typically the minimum field current is approximately 60 per cent of normal, and the maximum is approximately $1^1/_2$ normal.

This scheme of motor-field excitation provides three benefits:

1. Most important: an ample margin of torque stability is provided at all loads, both steady-state and transient.

2. A maximum speed of the empty hook is obtained which is moderately greater than that corresponding to the maximum no-load voltage of the generator.

3. Without any complication of contactors, relays, or the like in the motor-field circuit, the motor field continues strong at the "off" position of the controller during a retardation but is reduced to a value moderately less than normal while the hoist continues to stand at rest at the "off" position.

Because the cross-flux exciter is used for two purposes, its armature current is the *difference* of the generator-field and motor-field currents. Thus at zero "loop" current the exciter armature current is at its maximum value as output. At rated "loop" current the generator-field current and motor-field current are nearly equal; hence the exciter armature current is not far from zero. At maximum "loop" current, when the exciter voltage is low accordingly, most of the hoist-motor field current becomes input into the exciter which accordingly acts regeneratively. The uncorrected effect of armature IR drop would be to decrease the maximum effective voltage of the exciter by a more than negligible percentage, but also to increase the minimum voltage of the exciter by a large percentage over the desired low value. It is principally for these reasons that the exciter is compounded as has been mentioned.

Certain optional features were added in the developmental sample, and tests were made both with and without their use. It was decided that the modifications of characteristics provided by these additions were not of sufficient importance to justify their inclusion in typical equipments. For this reason and because of space limitations these features are not described.

Speed-Torque Characteristics

The characteristics of the system are determined principally by the manner in which the generator voltage varies, subject, however, to the modifying effect of the IR drop of the "loop" circuit and the additional modifying effect of the motor-field variation. When the generator field is excited and controlled as described, the characteristics of generator voltage and of motor counter electro-

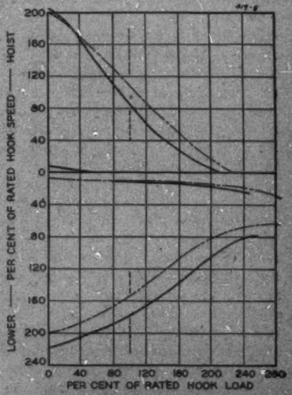

Figure 8. Calculated characteristics expressed as load on hook, from tests as in Figure 7

motive force with respect to "loop" current will resemble those of Figure 6, in which the curves of generator voltage represent not terminal voltage but voltage generated within the generator. In the upper right-hand quadrant, representing the hoisting of loads, the effect of "loop" circuit *IR* drop is subtractive from that of the generator voltage, but in the lower right-hand quadrant, representing the regenerative lowering of loads, this *IR* drop is additive. Hence the curves of motor counter electromotive force are not symmetrical in the hoisting and lowering direction, and upon any lowering position of the controller a stalled condition never occurs at any load, however large. If the hoist motor field were constant, the curves of motor counter electromotive force in Figure 6 would represent also the speed of the hoist motor. The effect of the variable motor field upon the speed-torque curves is to increase moderately the hoisting and lowering speeds at loads less than normal and to decrease slightly the speeds at overloads.

Figure 7 shows a set of speed-load characteristics from factory tests of the first equipment of this type. This equipment was a developmental sample for which only the cross-flux exciter, the control equipment, the brake magnet coil, and the hoist-motor-field winding were made for the purpose, the remainder of the equipment being borrowed from stock. The loading arrangement provided a "live" counter torque, produced electrically and adjustable over a wide range, which provided an authentic simulation of the various loads upon a hoisting hook. In accordance with typical crane practice five speed-control positions were provided in each direction. The characteristics shown by curves $1H$ to $5H$ and $1L$ to $5L$ drawn in solid lines were obtained when using the optional features mentioned. The simpler arrangement, omitting the optional features, provided the characteristics shown in dot-dash lines, labeled "alternative." The corresponding "alternative" characteristic for slowest hoisting was almost identical with the curve $1H$ shown. From the test data the characteristics of speed versus load on the hook were calculated for the slowest and fastest speeds, as shown in Figure 8. These characteristics were calculated on the basis of an assumed mechanical efficiency of 87 per cent. In the author's opinion the "alternative" characteristics shown by the dot-dash lines are to be preferred.

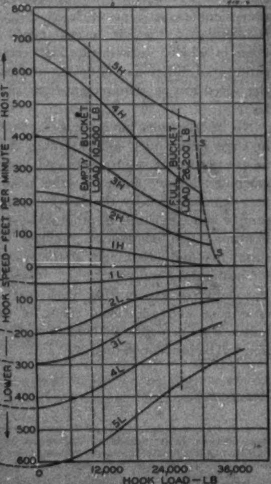

Figure 9. Characteristics of hoist of cantilever gantry crane, from tests of installation at Fontana Dam

The characteristics shown in Figure 7 and Figure 8 are considered to be particularly suitable for cranes in steel plants and heavy machine shops, subject to the possible exception that the highest-speed lowering characteristics there shown may be faster than considered desirable. It is a simple matter to readjust so as to reduce the speeds of the fastest lowering characteristic and to redistribute the intermediate characteristics accordingly.

The speed-torque characteristics upon the *lowest-speed lowering position*, curves $1L$ in Figure 7 and Figure 8 show a moderately higher speed when lowering rated load or overloads than when lowering the empty hook; hence they do not meet completely the sixth item in the list of ideal characteristics. However, these speed characteristics are superior in this respect to the characteristics $1L$ in Figures 1 and 2, which represent approximately the best of the standard practice heretofore. By improvements in design it is not difficult to make a further improvement over the characteristic $1L$ of Figure 7 without detracting from any other characteristics. This has been proved in the larger equipment of later design whose characteristics are shown in Figure 9.

Figure 10. Fontana Dam construction trestle equipped with two cantilever gantry cranes for handling four-cubic-yard concreting buckets, provided with variable-voltage hoist control using Whiting cross-flux exciter system and variable-voltage rack motion with floating-flywheel exciter

With the possible exception of an extraordinary combination of conditions a crane is never called upon to lower a hook load greater than that which it is capable of picking up and hoisting. However, in the factory tests of the developmental sample electric "live" loads were applied equivalent to hook loads substantially greater than any which could be lifted, as shown in Figure 8. At such excessive loads curves $1L$ show increases in speed. These, however, do not involve any tendency toward instability but are merely the result of increased *IR* drop of the "loop" circuit throughout the excessive overloads at which the generator voltage is at a nearly constant minimum. Throughout the entire overload range the hoist-motor excitation increases until at maximum overloads including those which are impossible in practice the motor is highly saturated, and hence in a very stable condition.

A large installation comprises two high-speed high-lift cranes used in the construction of a dam on the Little Tennessee River at Fontana, N. C. Although these cranes are very different from a typical steel-mill or heavy factory crane, the ideal characteristics enumerated in the introduction are especially desirable in this application. Because of the unusual character of these cranes certain additional requirements are imposed upon the speed-load characteristics of the hoist. Accordingly the electric equipment for the hoist was designed in accordance with the same principles but with certain changes in proportions and one additional feature.

These cranes are of a type sometimes called "hammerhead" but more aptly named cantilever gantry cranes. They have a maximum lift of 295 feet and at the extreme outward travel of the rack (that is trolley) motion the overhang of the hoisting hook at the end of either cantilever is 128 feet beyond the supporting tower. The hoist drive of each crane has a rms rating of 187 horsepower. These cranes are used at times for roustabout work, but their principal duty is the "pouring" of concrete.

During the earlier part of the construction schedule the crane picks up a bucket of freshly mixed concrete from a railroad car running between the legs of the crane tower, hoists the bucket sufficiently to clear the side rails, racks out the bucket to the required distance, and lowers it to the upper surface of the "block" being poured. When the bucket has been dumped, it is hoisted, racked in, and landed on the car. When the dam reaches nearly its maximum height, the loaded bucket will be lifted from the car a maximum of about 50 feet, and, after racking out, dumping and racking in, the empty bucket will be lowered to land on the car.

The purchase specification required that the speed of hoisting the loaded bucket be a relatively high percentage of the speed of hoisting the empty bucket but required also protection against an excessive overturning moment. Although the loaded bucket at its maximum overhang is entirely incapable of overturning the crane, it is possible when the crane is engaged in roustabout work that a hoisting hitch may be taken about an excessively heavy piece or an immovable object. Under the latter circumstances it is imperative that the hoisting effort shall be self-limiting to a value substantially less than the overturning moment of the crane.

In order to meet these requirements and at the same time retain the various other desirable characteristics, a cross-flux exciter was designed in which the two flux paths were purposely dissimilar whereby a set of speed-load curves was obtained as shown in Figure 9 based upon tests of the installation. In order to limit the stalled torque of the hoist motor to a value only moderately greater than its value for hoisting the maximum load in regular service, a simple addition was devised which causes the characteristics of the cross-flux exciter to break abruptly from the normal characteristic for which it is adjusted and at higher values of "loop" current to provide the stalling characteristic shown in Figure 9 by the broken lines *S–S*.

In addition to its adaptation to the special requirements just mentioned, the characteristics of this equipment provide two improvements over those of the developmental sample. The first-speed hoisting characteristic, curve 1*H*, is flatter. The first-speed lowering characteristic, curve 1*L*, goes all the way in meeting the seventh item in the list of ideal characteristics ". . . that the lowering speed be *less* at heavy loads than at partial loads or empty hook."

Transient Characteristics

As was predicted when the system was first devised on paper, the superior steady-state characteristics and the means by which these characteristics are obtained contribute to the creation of favorable transient operating characteristics during the course of ordinary accelerations, retardations, and reversals, and during emergency stops. Because the maximum speed when lowering a heavy load is much less than when lowering a light load or the empty hook, the kinetic stored energy of the hoisting system is inherently reduced whenever increased hook loads are being lowered. Under these conditions the generator-field current is also much less than maximum; hence at a quick movement of the controller from full speed to the "off" position (or to a hoisting position) the initial rate of decay of generator field is much less than maximum. At the heavier loads the motor field is strong, and an increase of "loop" current for the retardation causes a further increase of motor-field current. From the combination of these phenomena it comes about that the retardation of heavy descending load requires a "loop" current only moderately greater than that of the speed from which the retardation is begun and only a little greater than that for retarding the empty hook from its maximum speed.

This combination of phenomena is similarly and equally beneficial with respect to the retardation and stop under emergency conditions such as failure of incoming power. It has been observed that when maximum load is being lowered at its maximum speed an emergency stop requires only slightly more current from the hoist motor than is required for the corresponding emergency stop of the empty hook and little more than for a normal retardation at the maximum rate.

Conclusion

The hoisting-machinery art includes many different types of equipment for widely different applications. Some of these types of hoist have relatively unexacting requirements which are met acceptably by various simple systems of electric drive. Several systems of electric-hoist drives have been developed, each of which has certain specialized modifications which are particularly advantageous for certain exacting applications but which are irrelevant or disadvantageous for certain other hoisting applications whose requirements are equally exacting but different. The electric system described in this paper is not to be considered as preferable or even applicable for every important type of hoist, but it is intended for use in several types of hoist in which, notwithstanding certain substantial differences of design and use, the speed-torque requirements are similar.

No installations have yet been made in large heavy-duty indoor cranes such as are typical of steel plants and heavy machine shops. However, the results first demonstrated by the developmental sample and confirmed in practice by the Fontana Dam installation indicate the suitability and advantages of the system for heavy-duty indoor cranes. It seems reasonable to expect that with further experience and additional study of details and refinements the field of application will be broadened further.

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Gear Ratio and Its Effect on Traction Motors

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Synopsis: The mechanical power delivered by the motors of electrically propelled vehicles is almost always transmitted through gears to axles. The few exceptions are some gearless electric locomotives designed many years ago. Although the top speeds of vehicles and locomotives have increased materially since the inception of electric drive, the very nature of the service requires high tractive efforts in starting from standstill and at low speeds. In the case of transit vehicles, frequent stops require operation at relatively low speeds a large percentage of the time.

The history of traction-motor design discloses a trend toward higher and higher motor speeds. These higher speeds have resulted in reduced motor weight and space requirements, which are two most important considerations in traction apparatus. In order to use motors of higher speed economically, greater gear reductions are required. As developments in gear design, gear materials, and heat treatments progressed, greater gear reductions became practicable, and the higher-speed motors came into general use. Modern double-reduction gear units and hypoid gears permit greater gear reductions and, hence, higher motor speeds than the single-reduction spur gears which were once employed in all traction applications.

The reductions in weight and size of traction motors are not only advantageous in themselves, but they permit features in vehicle construction which add to the comfort and convenience of passengers and to over-all economy of operation. This paper discusses some of the elementary considerations of gear design and includes a specific example of the effect of change in gear ratio on traction motors.

THE electric motor is extremely well adapted to high-speed operation. The higher the speed at which it can be operated, within limits, the lighter and more compact the motor. The wheel speeds of electrically propelled vehicles and locomotives are quite low by comparison, because they have to start from zero speed at frequent intervals, and even at top vehicle speeds the wheel speed is still too low for economical gearless motor design. As a result, practically all propulsion motors are geared to the axles, the gear reduction serving the usual two-fold purpose of reducing the speed and multiplying the torque. As improvements in gear materials, heat treatments, and designs have made greater reductions practicable, it has been possible to utilize economically motors of higher speed. Streetcar motors serve as a good example of this trend, because they were the earliest traction motors, and continued progress has been required to keep streetcars competitive with other forms of transportation.

Early Motor Designs

All early traction motors were supported at one side by bearings on the axle and on the other by a connection to the car or locomotive truck. The distance between the center of the pinion, mounted on the motor shaft and the center of the gear, mounted on the axle was fixed for each motor frame. This gear-center distance is *SA* in Figure 1A. This distance is divided into two parts, *SB* and *BA*, so selected that *BA* divided by *SB* gives the speed reduction desired. The two circles shown with *A* and *S* as centers are known as pitch circles of the gear and pinion, respectively. The gear teeth are laid out around these circles.

Let us assume that the gear-center distance of a particular motor was 14 inches and the diametral pitch of the gears three. The total number of teeth in gear and pinion was $3\times2\times14=84$. The values selected are typical of the gears used on 40- and 50-horsepower 500-volt streetcar motors 35 to 40 years ago. At that time a 15-tooth three-diametral-pitch pinion was the smallest in general use, because a smaller pinion did not have sufficient strength between the root of the tooth and the bore, or at the keyway, with the best material available at that time.

In the example selected there must be $84-15=69$ teeth in the gear. The diameter over the ends of the gear teeth was $(69+2)/3=23.67$ inches. With new wheels a clearance of four inches between top of rail and bottom of gear case was considered essential to avoid hitting the gear case on obstructions in the street. The outside diameter of the gear case was approximately one inch greater than the diameter over the ends of the gear teeth to allow for clearance in the gear case and thickness of the gear case. Therefore, the diameter of the new wheel could not be less than $23.67+1+8=32.67$ inches, and 33-inch wheels were in common use. They were worn to a minimum diameter of 30 inches; thus, the clearance under the gear case was reduced by 1.5 inches. The limitations of pinion and gear design made gear reductions of 4.6:1 general and 33-inch wheels a standard for city cars.

The streetcars of this period weighed 50,000 pounds and were propelled by four 40- or 50-horsepower 500-volt motors, depending to a great extent on the grade conditions of the city in which they were operated. The clearance between the top and the rail and the bottom of the motor frame, with 33-inch wheels, was also approximately four inches. The 40-horsepower motor weighed 2,800 pounds, complete with gear and gear case, and 2,400 pounds without gear and gear case. The maximum safe speed of the motor was 1,900 rpm, which resulted in peripheral speeds of 7,000 feet per minute of the armature and 5,100 feet per minute of the commutator.

There was not much change in motor speeds for a number of years, although progress was made along other lines. Motors with commutating poles became standard, and ratings were based on 600 volts with a resulting increase in horsepower of 20 per cent. Bearings and shafts were improved. Better gear materials were used, and heat treating became general. Progress was made in insulating materials. During this period the traction motor progressed from the state where normal life of pinions, bearings, windings, brushes, and commutators was short to where life of these parts was increased several times.

New Motor Designs and Low-Floor Cars

The year 1912 marked a traction-motor and streetcar development which radically changed the appearance of streetcars and the ease with which passengers could board and alight from them. The Pittsburgh Railways Company designed a car with 24-inch wheels and correspondingly low-car floors. The car seated 55 passengers, and its weight was only 34,000 pounds. Four 35-horsepower 600-volt motors were adequate for even the severe grade and traffic conditions of Pittsburgh.

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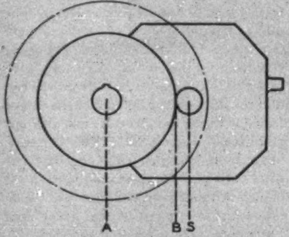
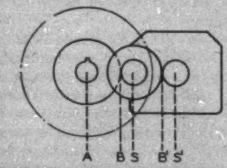
The design of a motor for 24-inch wheels was an undertaking of considerable magnitude at that time. The motor developed was known as the Westinghouse type 328. It was self-ventilated by means of a fan on the armature shaft, and in the original design the coils were "bakelized." Gears of 3.5 diametral pitch were used, and the face of the gears was four inches instead of five inches, as used previously. The use of the $3\frac{1}{2}$-pitch gears made it possible to get the required reduction in the space available. Because of progress in gear construction, these gears proved entirely satisfactory. The clearance under the motor frame was $3\frac{1}{4}$ inches and under the gear case three inches. This sacrifice of one-inch clearance meant that, for parallel street conditions, the wheels could not be worn to the same extent as on the older cars. Actually, the 24-inch wheels were later superseded as standard by 26-inch wheels, thus increasing clearances one inch.



A. Motor with single-reduction gears



B. Motor with double-reduction gears

Figure 1

The gear-center distance of the new motor was 10.43 inches, making a total of 73 teeth. A 15-tooth 3.5-pitch pinion was the minimum permissible. The maximum reduction-gear ratio was 58:15 or 3.87:1, but this gear reduction with 24-inch wheels is equivalent to a reduction of 5:32:1 with 33-inch wheels.

The maximum safe armature speed of the motor was 2,600 rpm. At this speed the peripheral speed of the armature was 6,800 feet per minute and of the commutator 5,800 feet per minute. While the maximum safe speed of the armature was raised 37 per cent, the peripheral speed of the armature was held essentially the same by reducing the diameter, and the peripheral speed of the commutator was increased only 14 per cent. The motor complete with gear and gear case weighed 1,680 pounds or a reduction of 40 per cent compared with the 50-horsepower 500-volt (60-horsepower 600 volts) motor used on the heavier cars. The total weight saving, due to the four lighter motors, was 4,480 pounds. The saving, due to using 24-inch instead of 33-inch wheels and the resulting lighter truck structure, was 4,000 pounds. Other weight economy was effected in the car body.

For the next 15 years street-railway motor design followed the trail blazed by this first motor suitable for low-floor cars. In a few years ventilated motors suitable for 26-inch wheels were available in 25-, 40-, and 50-horsepower sizes. A gear reduction of 5.7:1 was used on 25-horsepower motors, gears of $4\frac{1}{4}$ diametral pitch being employed. Again, there was a period of perfecting of details and consolidation of advances.

One of the latest axle-hung motors developed had a one-hour rating of 50 horsepower at 1,000 rpm. It weighed 1,940 pounds without gear and gear case and 2,185 pounds with gear and gear case. It had a gear reduction of 53:14 or 3.78:1 when gears of three diametral pitch were used. The maximum safe speed of armature was 3,000 rpm. The corresponding peripheral speed of the armature was 8,650 feet per minute and of the commutator 7,060. The car speed corresponding to 3,000 armature rpm was 61 miles per hour. While the peripheral speeds were raised at the maximum safe armature speed, the corresponding car speed was so high that it was rarely, if ever, reached in urban service.

Table I. Comparison of Traction Motors

Horsepower	Volts	Gear Ratio	Wheel Diameter—Inches	Total** Reduction	Maximum Safe Speed—Rpm	Peripheral Speed Feet Per Minute		Bare Motor Weight—Pounds Per Horsepower	Miles Per Hour at Maximum Rpm	Clearance (Inches)	
						Armature	Commutator			Motor	Gear Case
40	500	4.6 : 1	33	0.0213	1,900	7,000	5,100	60.0	40	$4\frac{1}{2}$	4
50	600	4.6 : 1	33	0.0213	1,900	7,000	5,100	47.8	40	$4\frac{1}{2}$	4
35	600	3.87 : 1	24	0.0185	2,600	6,800	5,800	42.6	48	$3\frac{1}{4}$	3
50	600	3.78 : 1	26	0.0204	3,000	8,650	7,060	38.8	61	$3\frac{3}{4}$	$3\frac{1}{2}$
35	300/600	9 : 1	22*	0.0073	5,000	11,100	9,500	16.3	36.5	5	$4\frac{1}{2}$
55	300/600	7.17 : 1	25	0.0104	5,000	12,100	9,300	12.7	52	6	$4\frac{1}{4}$

* With *WN* double-reduction drive.

** Rpm × total reduction = miles per hour

$$\text{Total reduction} = \frac{\text{wheel diameter}}{336 \times \text{gear ratio}}$$

Separate Gear Units

In 1927 a radical change in method of transmitting motor torque to the street-car axles was tried experimentally. Worm gears were used. The time-honored axle-hung motor, sleeve bearings, grease-lubricated spur gears and split gear case was a crude noisy arrangement by private automobile standards. It did function successfully. Two cars were constructed using worm-drive axles, including differential gears, which were an outgrowth of bus and truck axles. Oil-lubricated gears in a dirt-tight case were employed. The gears were silent. Gear ratios of 10:1 were available. The separation of the gear unit from the motor removed the restriction of gear center distance as a determining factor in gear ratio and allowed the most economical motor speed to be selected.

On the first experimental cars high-speed motors, developed primarily for gas-electric bus service, were applied. These motors were soon superseded by a high-speed railway motor specifically developed for the service. This motor rated 35 horsepower and had a maximum safe armature speed of 5,000 rpm. The maximum armature peripheral speed was 11,100 feet per minute, and the maximum commutator peripheral speed was 9,500 feet per minute. The motor weight was 570 pounds. Since the motors were entirely spring suspended, lighter-weight construction was more practicable than it was in former designs, where they were subjected directly to all the impacts of the wheels on rail irregularities. The worm-drive axles proved to be unsatisfactory in railway service, but they hastened the development of much-higher-speed motors than were previously used and other forms of gear drive which have proved satisfactory.

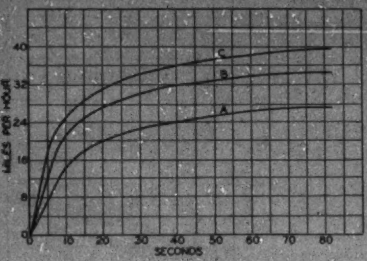


Figure 2. Speed–time curves

Car *A*. 48,000 pounds empty, 6,000 pounds passenger load, four 40-horsepower 500-volt motors, 69:15 gear ratio, 33 inches wheel diameter

Car *B*. 38,800 pounds empty, 6,000 pounds passenger load, four 50-horsepower 600-volt motors, 53:14 gear ratio, 26 inches wheel diameter

Car *C*. 33,000 pounds empty, 6,000 pounds passenger load, four 55-horsepower 600-volt motors, 7.17:1 gear ratio, 25 inches wheel diameter

The application of worm drive to streetcars was followed very quickly by the development of a double-reduction and a single-reduction self-contained gear unit which possessed most of the advantages of the worm drive. When the gear teeth were ground, the accuracy of tooth contact was improved. Antifriction bearings maintained gear-center distances accurately. As a result, the gear noise was reduced so greatly that the average passenger was not conscious of it. Also, gears of higher diametral pitch were used successfully. The double-reduction unit employed high-speed gears of $4^{1}/_{2}$ diametral pitch and the low-speed gears four. Phenomenally, long gear and bearing life was obtained. The spur gear units were more efficient than the worm drive.

The double-reduction drive permitted the use of wheels having a diameter of 22 inches with 4.5 inches clearance under the gear case. Figure 1B illustrates the double-reduction gear arrangement. The maximum gear reduction was 9:1, although most applications were made with less reduction in order to obtain the higher car speeds which were coming into general use. The single-reduction drive provided a maximum reduction of 7:1 and required 26-inch wheels to give $3^{3}/_{4}$ inches clearance between top of rail and bottom of gear unit.

Presidents' Conference Committee Car

Just at the time that these drives were proving their worth in service, the Electric Railways Presidents' Conference Committee was engaged in designing a completely new streetcar. New standards of performance in rates and smoothness of acceleration and braking, speed, passenger comfort, heating, ventilation, and lighting were established. Entirely new car body and car trucks, including rubber-cushioned wheels, were developed. A right-angle drive (motor shaft at right angles to axle) was selected, because it was better fitted to the truck design, was believed to be inherently quieter than the double-reduction spur gears, and was less expensive. The drive selected consisted of hypoid gears with 7.17:1 reduction. Antifriction bearings and oil lubrication were used. This drive embodied the advances of automotive-gearing design and has operated most successfully.

The new standards of performance required the design of a new motor having a one-hour rating of 55 horsepower. The four motors not only propel the car but do most of the braking dynamically. The dynamic braking is supplemented by electrically operated track brakes and air-operated brakes on the car wheels or electrically operated drum brakes on the armature shaft. The energy dissipated in the accelerating and braking resistors is used for heating the car during cold weather.

The new motor weighs 700 pounds and has a maximum safe speed of 5,000 rpm. At 5,000 rpm the peripheral speed of the armature is 12,100 feet per minute and of the commutator 9,300. The corresponding maximum car speed is 52 miles per hour. Wheels of 25-inch diameter are used. The clearance under the gear housing is $4^{1}/_{4}$ inches. The motors are completely spring-born, and, therefore, their height above the rail varies somewhat with passenger load. Normally, it is six inches.

For convenience of comparison Table I shows the principal characteristics of the various motors which have been discussed. They are typical of the streetcar motors used at various times in the past 40 years. Worthy of note are the increase in total reduction, the consistent increase in maximum safe speed, and in armature and commutator peripheral speeds. Although these changes did not accompany improvements in gearing step by step, they were in large measure the

Table II

Car	Schedule Speed (Miles Per Hour)	Kilowatt-Hours Per Car Mile
A	10.4	3.6
B	12.5	4.4
C	14.4	5.3

Table III

Gear Ratio	Schedule Speed (Miles Per Hour)	Kilowatt-Hours Per Coach Mile	Root Mean Square Amperes
9.2:1	15.7	2.7	156
11.4:1	15.2	2.4	131

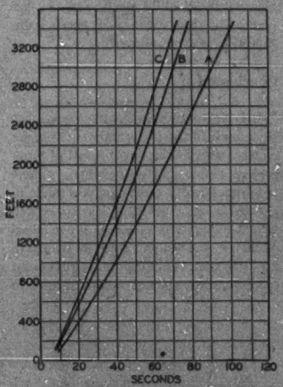


Figure 3. Distance–time curves

Cars *A*, *B*, and *C* as in Figure 2

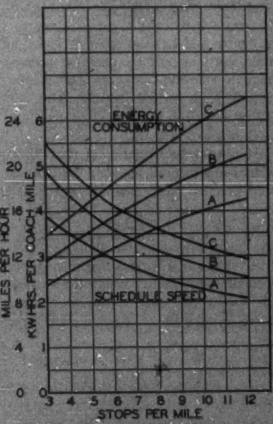


Figure 4. Operating-characteristic curves

Cars *A*, *B*, and *C* as in Figure 2

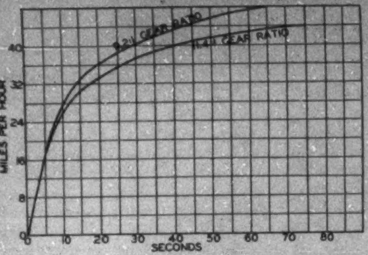


Figure 5. Speed-time curves

Trolley coach with 9.2:1 and 11.4:1 gear ratios

result of gradual gear evolution. Cast iron and malleable iron were superseded by cast steel, forged steel, and alloy steels. The 14½-degree involute tooth form was superseded by 20 degrees involute, thus increasing strength. Long and short addendum pinions and gears were adopted with resulting greater pinion strength and more metal between root of tooth and pinion bore. Therefore, smaller pinions can be used. Water quenching, oil quenching, case hardening, and flame hardening were introduced.

Figure 2 shows the comparative speed attained in a given time by three types of cars. Car *A* is one typical of those in use 40 years ago. Car *B* represents the best type of car with axle-hung motors available in the late 1920's. Car *C* is the modern PCC car. To attain a speed of 25 miles per hour requires 48 seconds for car *A*, 16 seconds for car *B*, and 10 seconds for car *C*.

Figure 3 shows the distance covered by each car for various moving times. A distance of 1,000 feet is traversed in 39 seconds by car *A*, 31 seconds by car *B*, and 27.5 seconds by car *C*.

Figure 4 shows the schedule speed in miles per hour and the energy consumption in kilowatt-hours per car-mile for various stops per mile between 3 and 12. The comparison at eight stops per mile is shown in Table II.

It should be understood that these values are theoretical, and, that, in actual practice both schedule speeds and energy consumption are lower for all three cars than calculated. Although the energy consumption of car *C* is relatively high, it affords a superior service which requires more power. High-schedule speeds not only appeal to the patrons, but the wages of car operators per car-mile vary inversely, as schedule speed and operators' wages are the principal item of operating expense.

Trolley-Coach Performance

The trolley coach is selected as the means of illustrating the effect of gear ratio on traction-motor performance for a given set of conditions. Modern streetcars are standardized to such extent that the same gear ratio is used in all applications. Various gear ratios are applied on trolley coaches. Figure 5 shows the comparative speeds of a typical trolley coach with gear ratios of 9.2:1 and 11.4:1. Trolley-coach gear ratios are greater than streetcar ratios, because the rubber-tired wheels have an effective diameter of 40 to 43 inches instead of 25.

Figure 6 shows the schedule speed and energy consumption for a typical trolley coach with each gear ratio. Not only are schedule speed and energy consumption increased by decreased gear reduction, but also motor rms current is increased. At six stops per mile the comparison is as indicated in Table III. The percentage increase in schedule speed is small compared with the increase in energy consumption and rms current.

Conclusion

It is apparent that great progress has been made in the performance of vehicles on the street as the direct result of progress in motor and gear design. There has been equally great progress made in car design as a result of less space and weight required for motors. When wheel diameters are reduced from 33 inches to 25 inches, car floor height is reduced a corresponding amount. This results in

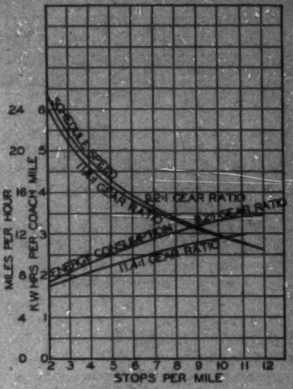


Figure 6. Operating-characteristic curves

Trolley coach with 9.2:1 and 11.4:1 gear ratios

one less step, which, in turn, means greater safety, less boarding and alighting time, shorter stops, and, hence, higher schedule speeds.

All are familiar with the outward changes in streetcars and trolley coaches. Unfortunately, too many examples of old street cars are in evidence for one to forget their outstanding characteristics. The more pleasing appearance, smoother and quieter operation, and better ventilation and heating in new streetcars are indirect results of progress in motor design.

Higher motor speeds brought with them improved commutator and armature construction and dynamic balancing. Better insulating materials, notably the general use of class-*B* instead of class-*A* insulation, and more effective ventilation, have made important contributions to reduced size and weight. Greater gear reduction has been essential to the economic application of these higher-speed motors.

References

1. Advantages of High-Speed Traction Motors, C. A. Atwell. AIEE Transactions, volume 62, 1943, July section, pages 508–10.

2. Modern Transportation Motors, H. W. Gouldthorpe. *Mass Transportation*, volume 36, March 1940, pages 64–6.

The Effect of Kilovar Supply on the Design of Systems for Load Growth

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THE benefits that can be obtained by the use of shunt capacitors in electric power systems have been discussed many times in the technical literature. Their ability to improve voltage and power factor in the distribution areas, thereby reducing losses and postponing the need for additional circuit capacity by reducing loading on existing equipment, has been used many times where the economics have justified such use.

Again, other discussions have shown that the greatest use of a given total system investment may be obtained when the basic kilovar requirements are supplied near the load by either capacitors or condensers. It is the purpose of the present paper to combine these two major thoughts by investigating the place of the capacitor in providing for load growth on a specific system. This approach will give some indication of the economics and the benefits to be gained on an over-all system basis, which in the final analysis is the most logical view to take.

In many cases kilovar supply by capacitors, for a single purpose, such as voltage improvement, reduction in transformer, line or cable loading, may not be justified, as other means may permit a lower first cost (as will be shown), but when these effects are viewed jointly and evaluated on a system-wide basis, a different conclusion may result.

Basis of Analysis

To permit a study of the possibilities of capacitors as a stepping stone in system growth, a type of power system using modern design principles was assumed. Although a synthetic system, electrical details of the circuits were worked out from the residential customer entrance and commercial and industrial customer transformer secondary circuits back to the generating plant. This was called the present system. Then each portion of the system was redesigned to enable it to carry a load increase which would be obtained at some future time, the new load and system being designated as future. The kilowatt portion of the load increase viewed at the generating plants amounted to 67 per cent, although this percentage varied somewhat for each individual portion of the system. The necessary design changes made in the present system to derive the future system capable of carrying the increased loads, were made to each of the various parts in two ways:

1. By replacing present conductors, transformers, and regulators with larger ones, or adding to such equipment.
2. By supplying part of the future load kilovars by capacitors, thus reducing the extent to which other equipment had to be increased.

Estimated installed cost figures were applied to the two methods for economic comparison, and the future system was set up both ways on the network analyzer to permit a comparison of over-all operating performance.

In general, the various feeder circuits were designed in either case with the same amount of margin for a still further increase in load, or so-called slack.

Conclusions

It will be realized that the area, within the system in which the use of capacitors shows an economic gain, depends very greatly on the character of the system in question. Where the unit or small substation principle is applied making the low-voltage heavily loaded circuit runs comparatively short, the gains are less in the distribution areas by themselves. This principle of system design tends to shift the major benefits from the distribution to other parts of the system.

The type of system selected in this study employed the unit- or small-substation principle quite extensively, as will be discussed more in detail later; so this fact should be borne in mind in considering the following conclusions. These conclusions must, of necessity, apply rigorously only to the system studied, although certain generalizations will be apparent from them.

1. Exclusive of the increased generating-plant facilities to care for the future requirements (which were the same in either case), the use of capacitors resulted in an over-all saving of approximately $262,400 on a $2,500,000 investment, or about 10 per cent.
2. The total estimated cost of changing only the feeder circuits to accommodate the increased loads, either with or without capacitors, was about the same. In one type of feeder the cost of using capacitors would be greater than that of increasing the materials or adding feeder voltage regulators, and so forth, whereas in another the relative cost would favor the capacitor, and, in the aggregate, on the system studied the cost was about a standoff.
3. The greatest differential in cost of increasing the capacity of the feeder circuits favoring the capacitor method occurred in the residential-type feeders, where the improvement of the first-to-last customer voltage of the single-phase lateral circuits with capacitors precludes the need of adding conductors and reconnecting for three-phase, which must be done for voltage purposes, if capacitors are not used.
4. The greatest differential in cost favoring not using capacitors was on the 33-kv feeders supplying industrial power at 33 kv where strictly no change to the feeder was required to enable it to carry the future load. A large capacitor bank was added, however, at the end of each of these for the capacitor case, because this would be a logical location in this system for a kilovar source in such a program.
5. The total estimated cost of changing the substation and subtransmission facilities, if capacitors were used on the feeders, was $139,500 as compared with $383,700 if capacitors were not used.
6. The operating margin in the generating plants under future load with capacitors was increased by a reduction in system losses of 7,900 kw and 34,000 kilovars on a system-delivered peak load of 255,000 kw. The additional margins provided would allow the system to operate with one less hydrogenerating unit under emergency conditions, if steam and prime-mover capacity were available. This is seen clearly in Figure 11.
7. It is well known that in systems using long heavily loaded feeders, capacitors offer the largest gains. This study indicates that in systems where such feeders are eliminated mainly by employing the unit- or small-substation principle of design, the use of kilovar source near the load still is justified on an over-all economic basis, from the standpoint of both first cost and operating efficiency. Thus, systems using modern design extensively may use to an advantage a co-ordinated system program of kilovar planning.
8. System incremental load losses are twice the actual per-unit load losses in the regions of practical system operation. This

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T. W. Schroeder, J. W. Butler, and N. H. Meyers are all of the central station engineering department, General Electric Company, Schenectady, N. Y., where Mr. Schroeder and Mr. Butler are sponsor engineers, and Mr. Meyers is in charge of the network analyzer.

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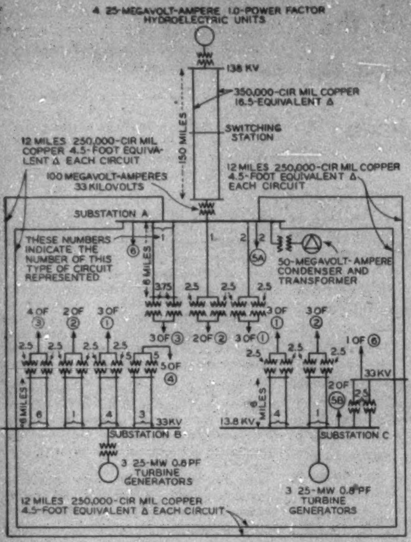

4 25-MEGAVOLT-AMPERE 1.0-POWER FACTOR HYDROELECTRIC UNITS
138 KV
350,000-CIR MIL COPPER 16.5-EQUIVALENT Δ
150 MILES
SWITCHING STATION
12 MILES 250,000-CIR MIL COPPER 4.5-FOOT EQUIVALENT Δ EACH CIRCUIT
100 MEGAVOLT-AMPERES 33 KILOVOLTS
12 MILES 250,000-CIR MIL COPPER 4.5-FOOT EQUIVALENT Δ EACH CIRCUIT
SUBSTATION A
THESE NUMBERS INDICATE THE NUMBER OF THIS TYPE OF CIRCUIT REPRESENTED
6 MILES
50-MEGAVOLT-AMPERE CONDENSER AND TRANSFORMER
4 OF ③
2 OF ②
3 OF ①
3 OF ③
2 OF ②
3 OF ①
5 OF ④
3 OF ①
3 OF ②
1 OF ⑥
2 OF ⑤B
33 KV
SUBSTATION B
13.8 KV
SUBSTATION C
3 25-MW 0.8 PF TURBINE GENERATORS
12 MILES 250,000-CIR MIL COPPER 4.5-FOOT EQUIVALENT Δ EACH CIRCUIT
3 25-MW 0.8 PF TURBINE GENERATORS

Figure 1. System layout. Present loads

All 33 kv is overhead. Six-mile circuits from generator to feeder substations are overhead; 4/0 copper 3.5 feet equivalent Δ spacing

Feeder Loads—Present

Circled Number	Type	Kilowatts	Kilovars
1	Residential	1,000	483
2	Commercial	1,000	750
3	Industrial	1,000	892
4	Commercial network	15–1,000	750
5*A*	13.8-kv industrial, 33-kv line	13,000	11,500
5*B*	13.8-kv industrial, 13.8-kv line	13,000	11,500
6	33-kv industrial, 33-kv line	14,000	12,400

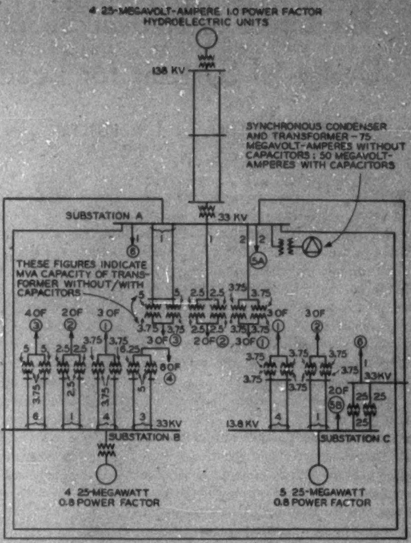

4 25-MEGAVOLT-AMPERE 1.0 POWER FACTOR HYDROELECTRIC UNITS
138 KV
SYNCHRONOUS CONDENSER AND TRANSFORMER – 75 MEGAVOLT-AMPERES WITHOUT CAPACITORS; 50 MEGAVOLT-AMPERES WITH CAPACITORS
SUBSTATION A
33 KV
THESE FIGURES INDICATE MVA CAPACITY OF TRANSFORMER WITHOUT/WITH CAPACITORS
4 OF ③
2 OF ②
3 OF ①
3 OF ③
2 OF ②
3 OF ①
8 OF ④
3 OF ①
3 OF ②
2 OF ⑤B
33 KV
SUBSTATION B
13.8 KV
SUBSTATION C
4 25-MEGAWATT 0.8 POWER FACTOR
5 25-MEGAWATT 0.8 POWER FACTOR

Figure 2. System layout. Future loads with and without capacitors. Other details same as Figure 1

Feeder Loads—Future

Circled Number	Type	Kilowatts	Kilovars	Kilovars With Capacitors
1	Residential	1,800	876	335
2	Commercial	1,800	1,350	870
3	Industrial	1,500	1,350	490
4	Commercial network	24–1,100	825	533
5*A*	13.8-kv industrial, 33-kv line	19,500	17,250	9,460
5*B*	13.8-kv industrial, 13.8-kv line	19,500	17,250	9,460
6	33-kv industrial, 33-kv line	21,000	18,600	10,140

applies for circuits that maintain voltage by kilovar supply at either end, as well as for circuits using a regulator at the sending end to maintain receiver voltage.

Statement of the Problem

Description of System

1. *General.* The system was set up and studied, as shown in detail in Figures 1 and 2. It is composed of three major load areas, each with a present load of about 50,000 kw and each connected to the other by a double-circuit 33-kv belt line 12 miles in length. Two of the load areas are supplied by a local steam-generating plant each, and the third by a hydroelectric plant 150 miles distant supplying its kilowatt requirements over a two-circuit 138-kv line, with an intermediate switching station, needed for transient stability. Kilovar requirements of this load area are supplied by a synchronous condenser feeding the 33-kv substation bus. Each generating plant is capable of carrying the load requirements of its own area; hence, power flow over the belt lines is restricted to emergency conditions. Most systems would generally operate part of its subtransmission facilities loaded during normal peak-load conditions, the effect of which would be to favor the capacitor case; so the results are considered very conservative.

2. *Feeders.* Types, loads, and distribution among substations: Feeder types and loads are classified in the tabulations accompanying Figures 1 and 2 for present and future loads, respectively. The design and performance details of each type of feeder, for present load, future load without capacitors, and future load with capacitors, are given in Figures 3 to 9, inclusive.

Table I indicates the distribution of types of loads and feeders among the three substations.

For purposes of this study it was deemed satisfactory in the interests of simplicity to use the same feeder of each type at all three substations. This, of course, is not a condition that would occur in practice, but if the feeder length and load are considered an average value, the results produced should reasonably approach those that would obtain on an actual system.

Alternative Methods of Handling Load Growth

Figures 3 through 9 indicate the alternative methods of providing for future load growth in the various feeders, and the detailed changes required for each method are listed in Table II. For the no-capacitor case the addition or enlargement of circuits and transformers and/or voltage regulators was resorted to in order to overcome the voltage drop or thermal-capacity limitations. For the capacitor case these limitations were overcome entirely or in part with capacitors, depending upon the details of each individual case.

These feeders were then represented as loads on the system, as shown in Figure 2, set up on the network analyzer. The normal and emergency performance of the system could then be studied with and without capacitors, and the effect of these capacitors on the system evaluated in terms of subtransmission system and distribution substation capacity needed. Table III and Figure 2 summarize the detailed changes needed to the system to enable it to carry the future loads of the feeders designed with and without capacitors.

Present and Future Loads

Details of individual feeder designs are given in the figures; hence, they need not be discussed here. It may be noted that the margin for growth beyond present loads is not necessarily the same in each of the feeders, nor is the percentage of growth to future loads exactly the same among the feeders. This fact does not affect the qualitative result of the economic comparison between the use of capacitors and the use of other means to provide for the load growth, but it obviously might affect the degree of the economic unbalance of the two methods. The values of present and future loads were taken at random; since, on an actual system they could, of course, be any value over a given period of time and would probably not be the same on each of the different types of feeders.

It is fair to expect, however, that feeder and system capacity be reasonably equally used up, that is, the margin for growth beyond future loads be the same, regardless of the method used to provide for the load increases. This is, in general, true, as will be shown by the following brief comparative comments on the margin remaining in each type of feeder, for the two methods. The numbers identifying the feeder types in Figure 3 through 9 correspond to the following numbered sections:

1. *Residential Feeder.* Changing single-phase laterals to three phase to cut down unregulated voltage drop in these sections reduces the voltage drop more than the simple addition of enough capacitors at the end of existing single-phase laterals to make the voltage drop acceptable, as was done in the capacitor case. This is not the discrepancy it first appears to be, however, because the future load is about the ultimate for the load area (as indicated by the load density) served by the feeder, and, even if this were not the case, the cost of achieving the same voltage drop with capacitors, as compared with changing over to three-phase laterals, would still be less.

Table I. Types of Feeders, Feeder Loads, and Distribution of Load Among Substations

			Load of This Type at This Substation								
			Present			Future					
						Without Capacitors			With Capacitors		
Feeder Number	Feeder Type	Number at This Station	Kilowatts	Kilovars	Power Factor	Kilowatts	Kilovars	Power Factor	Kilowatts	Kilovars	Power Factor
			Substation A								
1	Residential	6	6,000	2,890	0.91	10,900	5,370	0.896	10,870	2,044	0.985
2	Commercial	2	1,988	1,496	0.80	3,620	2,760	0.795	3,600	1,740	0.90
3	Industrial	3	3,030	2,610	0.758	4,490	3,370	0.80	4,370	1,210	0.975
4	Commercial network	0									
5a	Industrial HTP**	2	24,880	21,560	0.756	39,800	34,500	0.755	39,000	18,300	0.895
5b	Industrial HTP	0									
6	Industrial HTP	1	13,970	12,190	0.753	21,200	18,300	0.755	20,900	9,900	0.904
Total			49,868	40,746	0.774	80,010	64,300	0.778	78,740	35,396	0.914
			Substation B								
1	Residential	12	12,020	5,860	0.898	21,800	11,200	0.89	21,900	4,008	0.983
2	Commercial	2	2,018	1,506	0.80	3,590	2,710	0.798	3,600	1,760	0.897
3	Industrial	24	23,840	20,500	0.758	36,300	27,000	0.802	36,000	11,800	0.95
4	Commercial network	15/24*	14,960	11,490	0.793	26,600	19,700	0.803	26,400	12,800	0.90
5a	Industrial HTP	0									
5b	Industrial HTP	0									
6	Industrial HTP	0									
Total			52,838	39,356	0.802	88,290	60,610	0.823	87,900	30,368	0.945
			Substation C								
1	Residential	12	12,320	6,060	0.896	22,100	10,800	0.897	21,900	4,308	0.90
2	Commercial	3	2,992	2,239	0.80	5,500	4,000	0.808	5,470	2,570	0.904
3	Industrial	0									
4	Commercial network	0									
5a	Industrial HTP	0									
5b	Industrial HTP	2	26,080	22,960	0.75	39,000	34,500	0.749	38,900	18,800	0.90
6	Industrial HTP	1	13,960	11,890	0.761	21,200	18,000	0.762	21,000	10,200	0.90
Total			55,352	43,149	0.788	87,800	67,300	0.795	87,270	35,878	0.927

*Number of feeders with future loads.

**HTP stands for "high-tension (voltage) power."

2. *Commercial Feeder.* Cable ampere capacity is the limitation to increased load. If a parallel cable equal in size to the original were run, the capacity would be increased from 275 amperes to 550 amperes, whereas the capacitors added simply brought the future uncorrected load current of 312 amperes back to 275 amperes. Thus, by adding the cable, provision would be made for an added load of 1,700 kva above the 1,000-kva increase.

3. *Industrial Feeder.* Voltage drop limits load on this type feeder. A regulator of proper ampere capacity for future load is installed when the load grows. The regulator rating is the limiting factor for further growth. Similarly, only enough capacitors are added in the capacitor case to hold the voltage drop to the adopted values. No margin is thus left for additional load without adding equipment for either case.

4. *Commercial Network.* Capacitors are added in capacitor case at the four-kilovolt network supply busses without changing the network design; hence, there is no difference in margin between the two cases.

5a. *13.8-Kv Consumer Fed From 33 Kv.* Additional transformer capacity was added for a 50 per cent load increase such that, with the addition of fans, a 100 per cent load increase could be taken on the basis of one line and one transformer out; 2,600 kva of capacitors could be added in the capacitor case with an emergency (one line and transformer out) full-load voltage drop of 12 per cent, but, in the no-capacitor case, emergency operation at 100 per cent load increase would result in a current through the

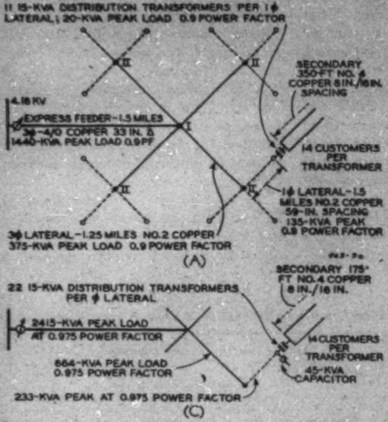

11 15-KVA DISTRIBUTION TRANSFORMERS PER 1ϕ LATERAL; 20-KVA PEAK LOAD 0.9 POWER FACTOR
SECONDARY 350-FT NO. 6 COPPER 8 IN./16 IN. SPACING
4.16 KV
EXPRESS FEEDER–1.5 MILES
3ϕ–4/0 COPPER 33 IN. Δ
1440-KVA PEAK LOAD 0.9 PF
14 CUSTOMERS PER TRANSFORMER
1ϕ LATERAL–1.5 MILES NO. 2 COPPER 59-IN. SPACING 135-KVA PEAK 0.9 POWER FACTOR
3ϕ LATERAL–1.25 MILES NO. 2 COPPER 375-KVA PEAK LOAD 0.9 POWER FACTOR
I
II
III
(A)
SECONDARY 175-FT NO. 4 COPPER 8 IN./16 IN.
22 15-KVA DISTRIBUTION TRANSFORMERS PER ϕ LATERAL
2415-KVA PEAK LOAD AT 0.975 POWER FACTOR
14 CUSTOMERS PER TRANSFORMER
664-KVA PEAK LOAD 0.975 POWER FACTOR
45-KVA CAPACITOR
233-KVA PEAK AT 0.975 POWER FACTOR
(C)

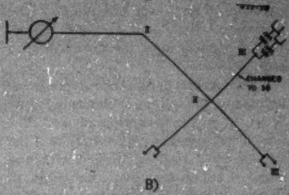
Figure 3. Residential feeder—type 1

(A). Present
Voltage drops at feeder peak:
Secondary three per cent
Distribution transformer 3.2 per cent
One-phase lateral 3.9 per cent
Three-phase lateral 2.9 per cent } *
Express section 6.8 per cent }
* Regulator compensates for these

Type 1 feeder. Present diversified peak at system peak 1,110 kva at 0.9 power factor = $1{,}000-j483$ kw (neglecting losses)

(C). Future with capacitors. Otherwise same as present
Voltage drops (at feeder peak)
Secondary 1.5 per cent
Distribution transformer 3.2 per cent
One-phase lateral 5.3 per cent (7.3−2 due capacitor)
Three-phase lateral 5.5 per cent } *
Express section 12.3 per cent }
* Regulator compensates for these

Type 1 feeder future diversified load at system peak = 1,830 kva at 0.985 power factor = $1{,}800-j316$ kw (neglecting losses)

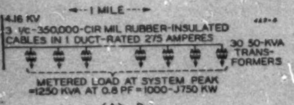

I
II
III
CHANGED TO 3ϕ
B)

(B). Future without capacitors. Otherwise same as present
Drops (at feeder peak):
Secondary 1.5 per cent
Distribution transformer 3.2 per cent
Three-phase lateral II–III 1.3 per cent
Three-phase lateral I–II 5.5 per cent } †
Express section 12.3 per cent }
† Type 1 feeder future diversified load at system peak = 2,010 kva at 0.9 power factor = $1{,}800-j876$ kw (neglecting losses)

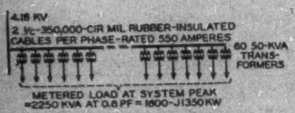
single line of about 600 amperes with a 13.6 per cent voltage drop.

5b. 13.8-Kv Consumer Fed From 13.8 Kv. Voltage drop under emergency condition of one circuit out with future load is considered about maximum permissible and nearly the same for either case; hence, there is no room for increased loads either way without additional investment.

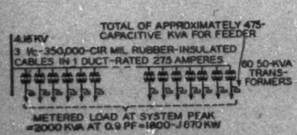
6. 33-Kv Consumer Fed From 33 Kv. No change to this feeder was necessary for it to carry the future load, but for the capacitor case a bank was added arbitrarily at the end of the feeder because of the large load kilovar requirement. This would permit about 8,800 kva of load at the original power factor to be added after future load arrived in the capacitor case, but none could be added in the no-


4.16 KV
← 1 MILE →
3 1/C–350,000-CIR MIL RUBBER-INSULATED CABLES IN 1 DUCT–RATED 275 AMPERES
30 50-KVA TRANSFORMERS
METERED LOAD AT SYSTEM PEAK =1250 KVA AT 0.8 PF = 1000–J750 KW

(A). Present

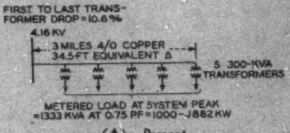

4.16 KV
2 1/C–350,000-CIR MIL RUBBER-INSULATED CABLES PER PHASE–RATED 550 AMPERES
60 50-KVA TRANSFORMERS
METERED LOAD AT SYSTEM PEAK =2250 KVA AT 0.8 PF = 1800–J1350 KW

(B). Future—no capacitors

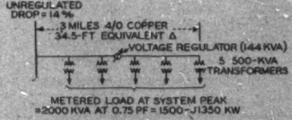

TOTAL OF APPROXIMATELY 475-CAPACITIVE KVA FOR FEEDER
4.16 KV
3 1/C–350,000-CIR MIL RUBBER-INSULATED CABLES IN 1 DUCT–RATED 275 AMPERES
60 50-KVA TRANSFORMERS
METERED LOAD AT SYSTEM PEAK =2000 KVA AT 0.9 PF = 1800–J870 KW

(C). Future—with capacitors

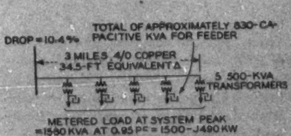
Figure 4. Commercial feeder, 4,160 volts—type 2


FIRST TO LAST TRANSFORMER DROP = 10.6 %
4.16 KV
3 MILES 4/0 COPPER 34.5-FT EQUIVALENT Δ
5 300-KVA TRANSFORMERS
METERED LOAD AT SYSTEM PEAK =1333 KVA AT 0.75 PF = 1000–J882 KW

(A). Present

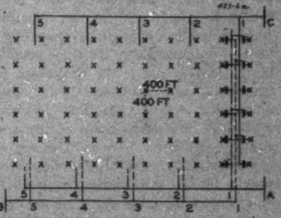

UNREGULATED DROP = 14 %
3 MILES 4/0 COPPER 34.5-FT EQUIVALENT Δ
VOLTAGE REGULATOR (144 KVA)
5 500-KVA TRANSFORMERS
METERED LOAD AT SYSTEM PEAK =2000 KVA AT 0.75 PF = 1500–J1350 KW

(B). Future—no capacitors

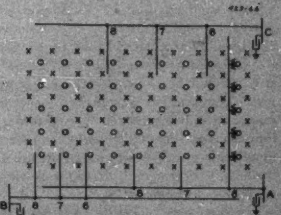

DROP = 10.4 %
TOTAL OF APPROXIMATELY 830-CAPACITIVE KVA FOR FEEDER
3 MILES 4/0 COPPER 34.5-FT EQUIVALENT Δ
5 500-KVA TRANSFORMERS
METERED LOAD AT SYSTEM PEAK =1580 KVA AT 0.95 PF = 1500–J490 KW

(C). Future—with capacitors

Figure 5. Industrial feeder, 4,160 volts—type 3


5
4
3
2
1
C
400 FT
400 FT
B
A

(*a*). Present—15 primary (4 kv) feeders—three-conductor 350,000 circular-mil cable average metered feeder load at system peak = 1,250 kva at 0.8 power factor. x indicates feed-in point to secondary network through 500-kva 4,160/120/208-volt transformer. Secondary grid uses two 250,000-circular-mil single-conductor cables per phase



(*b*). Future with and without capacitors shows added feeders only. Add nine three-conductor 350,000-circular-mil feeders. Average load of added feeders = 1,575 kva at 0.8 power factor. o indicates added feed-in points to added network through 500-kva 4,160/120/208-volt transformer. For with-capacitor case add 2,340 capacitive kilovolt-amperes at each of the three 4,160-volt supply busses, *A*, *B*, and *C*

Figure 6. Commercial network—type 4

Table II. Summary of Physical Changes in Feeder Circuits and Their Estimated Installed Costs to Handle Future Loads

Feeder Number	Feeder Type	Total Number This Type	Changes to Feeder Without Capacitors	Cost (Dollars)	Changes to Feeder With Capacitors	Cost (Dollars)	Total Cost (Thousands of Dollars)	
							Without Capacitors	With Capacitors
1	Residential	30	1. Split the secondary runs		1. Split the secondary runs			
			2. Add 132 15-kva 2,400/120–240-volt transformers	24,000	2. Same as for without capacitors	24,000		
			3. Change 18 miles of one-phase lateral to three-phase and reconnect half or original transformers	10,500	3. Add 540-kva outdoor distribution capacitors in 45-kva banks with hangers and one cutout per bank	2,300		
			4. Add 2,880 kva, 10 per cent induction regulator	10,000	4. Same as for without capacitors	10,000	1,335	1,089
2	Commercial	7	1. Add one single-conductor 350,000-circular mil varnished-cambric lead-covered cable per phase in existing one-mile duct	10,400	1. Add 498 kva of 7.5 kva 230 volts at 208-volt capacitor units in nine banks of nine units each with cutouts	9,300		
			2. Add 30 50-kva 4,160/120–208 volt transformer with cutout	11,500	2. Add 30 50-kva transformers	11,500	153.3	145.6
3	Industrial	27	1. Add 1,400 kva 10 per cent induction regulator	5,500	1. Add 900 kva of outdoor capacitors in 180-kva banks with hangers and cutouts	5,000	148.5	135
4	Commercial network	15 present 24 future	1. Add nine 3-conductor 350,000-circular mil cable feeders 2,000 feet long in new ducts	60,300	1. Same as without capacitors	60,300		
			2. Add 45 500-kva network transformers and protectors and vaults	517,500	2. Same as without capacitors	517,500		
					3. Add three banks of 2,340 kva of capacitors in housed racks at low side (1,460 volts) of feeder transformers with breakers	46,000	577.8	623.8
5a	13.8-kv customer fed by 6-mile 33-kv line	2	1. Add two 10-megavolt-ampere 33/13.8-kv transformers, including installation and station additions	44,000	1. Add two 7.5 megavolt-ampere 33/13.8-kv transformers, including installation and station additions	35,000		
					2. Add three 2,520-kva capacitor banks	49,600	88	169.2
5b	13.8-kv customer fed by 5-mile 13.8-kv lines	2	1. Add one 5-mile, 250,000 circular mil copper 13.8-kv line	26,500	1. Add three 2,520-kva capacitor banks	49,600		
			2. Add two 1,000,000-kva 15-kv circuit breakers, including station additions	22,000			86	99.2
6	33-kv customer fed by 6-mile 33-kv lines	2			1. Add 8,280-kva capacitor bank	54,300		108.6
							2,388.6	2,370.4

capacitor case because of voltage drop and conductor thermal limitations.

It may be noted that for feeder type 2 the no-capacitor case shows the larger margin, whereas in type 6 the reverse is true. Also a more favorable emergency operating condition obtains under ultimate load conditions for feeder type *5a* for the capacitor case than for the no-capacitor case.

Summary of Results

System Investments to Provide for Load Growth

In going from the present to the future system it was necessary to add the same amount of generating capacity at stations *B* and *C* with either method of providing for load growth. In view of this, the cost of the plant additions was not included in the comparison. This addition was one 25-megawatt unit at substation *B* and two 25 megawatt units at substation *C*.

The installed cost of changes to the feeders, substations, and subtransmission facilities to carry future loads with and without capacitors is itemized in Tables II and III. It is realized that the costs selected may differ considerably from those which would obtain on specific actual systems because of local conditions, but it is believed that the costs chosen are representative. A brief over-all summary of the investment results may be found in Table IV.

It is again emphasized that the major gains indicated in Table IV appear in parts of the system other than the feeders because of the principle of system design adopted. Small substations with relatively short feeders are the general principles involved. This has generally been called the unit-substation or load-center type of distribution. This principle results in a relatively efficient distribution system with fewer of the properties usually conducive to capacitor gains, and, as seen, any substantial benefit must necessarily appear elsewhere in the system. The loss data given in the following section further indicate the high efficiency of this system in relation to the average actual system being operated. The loss at time of peak for the future case without capacitors is only

Table III. Summary of Physical Changes to Transmission Facilities and Estimated Installed Costs

Changes If Capacitors Are Not Used	Cost (Dollars)	Changes If Capacitors Are Used	Cost (Dollars)
1. Add 25 megavolt-ampere synchronous condenser, transformer, and circuit breaker	212,000	Add 12 2,500-kva 33/4.16-kv transformers Add 5 2,500-kva 13.8/4.16-kv transformers	102,000 37,500
2. Add fans to two 3,750-kva transformers for 5,000 kva at 950	1,900		
3. Add six 2,500-kva 33/4.16-kv transformers	51,000		
4. Add six 5,000-kva 33/4.16-kv transformers	78,000		
5. Add fans to three 5,000-kva units for 6,250 kva at 1,100	3,300		
6. Add five 2,500-kva 13.8/4.16-kv transformers	37,500		
Total	383,700	Total	139,500

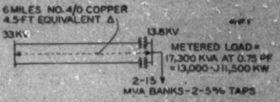
0.13 per unit. Hence, one should not conclude too hastily that the gains (as to magnitude and location) obtained in this system through a general kilovar program will apply to his particular system. Power systems having the more usual type of distribution, that is, longer feeders, will show comparatively greater benefit to the distribution system itself from the application of capacitors thereto.

SYSTEM LOSSES

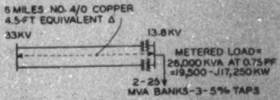
Table V summarizes the metered and generated loads and losses for the various cases.

The difference in load or metered kilovars between the capacitor and no-capacitor cases represents the kilovars of capacitors employed on the feeders and at distribution substations. This is 91,100 kva. The difference in kilovars supplied by generators for the two cases is 125,100 kilovars, so a kilovar loss of 34,000 obtains for the no-capacitor case.

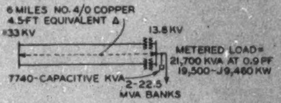
The value of the reduction in losses of 7,900 kw and 34,000 kilovars, resulting from the load kilovars being supplied near the load, was not accounted for in the economic comparison because of the considerable variation in what is considered a fair cost for losses among engineers. At any given rate, either on a capitalized investment or energy charge basis, the power losses may, of course, be evaluated readily. Similarly, the reactive power losses may be charged for on a basis of system investment required to supply them, or simply in terms of the cost of capacitors or condensers of ratings equal to the difference in losses. In any event, this difference in losses represents a difference in operating margins, and may be evaluated accordingly.


6 MILES NO. 4/0 COPPER
4.5-FT EQUIVALENT Δ
33KV
13.8KV
METERED LOAD=
17,300 KVA AT 0.75 PF
=13,000-J11,500 KW
2-15
MVA BANKS-2-5% TAPS

(A). Present. Drop with full load on one circuit=10.9 per cent

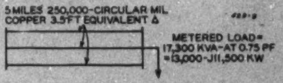

6 MILES NO. 4/0 COPPER
4.5-FT EQUIVALENT Δ
33KV
13.8KV
METERED LOAD=
26,000 KVA AT 0.75 PF
=19,500-J17,250 KW
2-25
MVA BANKS-3-5% TAPS

(B). Future—no capacitors. Drop with full load on one circuit=13.6 per cent

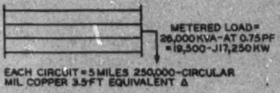

6 MILES NO. 4/0 COPPER
4.5-FT EQUIVALENT Δ
33 KV
13.8 KV
METERED LOAD=
21,700 KVA AT 0.9 PF
19,500-J9,460 KW
7740-CAPACITIVE KVA
2-22.5
MVA BANKS

(C). Future with capacitors. Drop with full load on one circuit=9.1 per cent

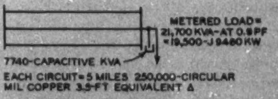
Figure 7. 13.8-kv industrial customer fed from 33 kv—type 5a


5 MILES 250,000-CIRCULAR MIL
COPPER 3.5-FT EQUIVALENT Δ
METERED LOAD=
17,300 KVA-AT 0.75 PF
=13,000-J11,500 KW

(A). Present. Drop with full load on two circuits=13.7 per cent


METERED LOAD=
26,000 KVA-AT 0.75 PF
=19,500-J17,250 KW
EACH CIRCUIT=5 MILES 250,000-CIRCULAR
MIL COPPER 3.5-FT EQUIVALENT Δ

(B). Future with no capacitors. Drop with load on three circuits=13.7 per cent


METERED LOAD=
21,700 KVA-AT 0.9 PF
=19,500-J 9460 KW
7740-CAPACITIVE KVA
EACH CIRCUIT=5 MILES 250,000-CIRCULAR
MIL COPPER 3.5-FT EQUIVALENT Δ

(C). Future with capacitors. Drop with full load on two circuits=14 per cent

Figure 8. 13.8-kv industrial customer fed from 13.8 kv—type 5b

GENERATING-PLANT MARGINS—NORMAL AND EMERGENCY OPERATION

Table III shows no change in generating capacity, because, for the system studied, the future kilowatt requirements demanded the same number of generators at stations *B* and *C* whether or not distribution capacitors were used. Similarly, the hydroelectric plant capacity required would be the same, since economics and operating requirements normally tend toward practically unity-power-factor long-distance transmission. However, at substation *A* a larger synchronous condenser is needed for the no-capacitor case to supply the required kilovars to this load area.

The difference in kilovar burdens and system-loss requirements on steam-generating plants *B* and *C*, therefore, evidences itself in this study in greater margins for future growth and for emergency conditions. The evaluation of this is left to the reader, but actually it could have been, and normally would be, evaluated in terms of the cost of generator or kilovar supply capacity installed at the station. If the generators can carry added kilowatt load when their kilovar load is reduced, it could be measured in terms of generator capacity costs.

In future steam-station loads with no capacitors, normal margin is one machine (spinning reserve) of $25-j\,18.75$ megawatts. (The minus sign on reactive quantities represents magnetizing reactive kilovolt-amperes throughout this paper.)

In future steam-station loads with capacitors, normal margin is $31.7-j\,61.9$ megawatts, which includes the unit held in spinning reserve.

On this basis, if it is assumed that the boilers and steam-unit prime movers have sufficient capacity, the generators can supply the load requirements under low-water conditions with two hydroelectric

Table IV

Item	With Capacitors	Without Capacitors
Cost of changes to feeders for a 100,000-kw system load increase	$2,370,400	$2,388,600
Cost of changes to rest of system, exclusive of power plant for this load increase	$ 139,500	$ 383,700
Total cost, exclusive of power plant	$2,509,900	$2,772,300
Cost per kilowatt of added transmission- and distribution-system capacity	$ 25.10	$ 27.72

Table V. Summary of Metered and Generator Loads

	Station	Megawatts	Reactive Megavolt-Amperes
Present metered loads	*A*	49.87	40.75
	B	52.84	39.36
	C	55.35	43.24
	Total	158.06	123.35
Present generation	*A*	58.0	8.2
	B	57.5	52.0
	C	58.8	46.0
	Condenser		51.5
	Total	174.3	157.7
		158.1	123.4
	Losses	16.2	34.3
Future without capacitors loads	*A*	80.01	64.3
	B	88.29	60.6
	C	87.9	67.3
	Total	256.1	192.2
Generation	*A*	91.5	4.9
	B	100.0	88.8
	C	98.0	82.0
	Condenser		76.0
	Total	289.5	251.7
		256.1	192.2
	Losses	33.4	59.5
Future with capacitors loads	*A*	78.7	34.19
	B	87.9	31.27
	C	87.3	35.68
	Total	254.0	101.14
Generation	*A*	86.2	−10.9
	B	97.6	44.9
	C	95.7	45.8
	Condenser		46.8
	Total	279.5	126.6
		254.0	101.1
	Losses	25.5	25.5
Losses without capacitors		33.4	59.5
Losses with capacitors		25.5	25.5
Reduction in losses with capacitors		7.9	34.0
Metered kilovar load without capacitors (future)			192.2
Metered kilovar load with capacitors (future)			101.1
Capacitor kilovolt-amperes added			91.1

units off if capacitors are not used, and with three hydroelectric units off if they are used. This is seen readily from the data in Figure 11 and is an example of how the generating-plant margins may be evaluated in terms of operating conditions.

Capacitors as a Stepping Stone in System Growth

The analysis reported in this paper was made on the basis of considering the cost of an addition to a system, as though its load grew 67 per cent, and the changes were almost overnight. No account was taken of the effects of providing for this ultimate increase in steps, somewhat ahead of an increment of load growth. It was assumed that this effect would be the same whether the capacitor or no-capacitor method were used in providing for the load increment. Obviously, this would be difficult of evaluation, unless a knowledge of the load growth or trend with time were available. Such knowledge is, however, available on actual systems and should be made use of for the purpose described.

Table VI. Δ Kilovars Per Δ Kilovolt-Ampere Per 0.05 Step in Power Factor

Δ Power Factor	Δ Kilovars Per Δ Kilovolt-Ampere
1.0 –0.95	6.2
0.95–0.90	2.68
0.90–0.85	2.05
0.85–0.80	1.8
0.80–0.75	1.59

The increase in kilovar capacity per kilovolt-ampere capacity in going from 0.95 to 0.90 power factor is 2.68.

Table VII. Generator Kilovolt-Amperes and Power Factor for Various Line Reactance and Load Values for Constant Kilowatts

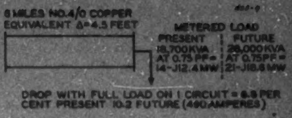
	Circuit Reactance = 0.20		Circuit Reactance = 0.30	
Load Power Factor	Generator Kilovolt-Amperes	Generator Power Factor	Generator Kilovolt-Amperes	Generator Power Factor
1.0	1.02	0.981	1.042	0.96
0.95	1.13	0.886	1.18	0.848
0.90	1.21	0.828	1.27	0.788
0.85	1.29	0.773	1.36	0.736
0.80	1.39	0.72	1.45	0.69
0.75	1.47	0.68	1.54	0.648

Actually, if this effect were evaluated, it would favor the use of capacitors, since they can be added in smaller quantities and follow the load growth more closely than regulators, transformers, lines, and cable circuits necessarily are able to do. Thus, there would be at any time somewhat less unused capacity lying idle waiting for the load to grow and use it up.

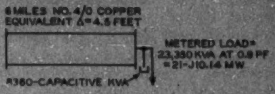
Another way of saying this is that the capacitor postpones the investment until such time as the larger equipment can be added justifiably, and it is in this way that capacitors may come to be used more in meeting system growth. Their high re-use value and great flexibility will permit them to be used on one part of a system for a time until it is enlarged and then to be moved elsewhere to repeat the process of following the load growth there.

Importance of Complete System Viewpoint

As were pointed out in the opening paragraphs of this paper, there are cases where, to meet a single requirement, and with alternate equipment available, the use of capacitors would not be justified. A study of the detailed feeder cost comparisons will show examples of this. There are two cases (feeders 4 and 6) where capacitors were not needed on the feeders themselves, but they were added at this location, as it was the logical place for kilovar production in the coordinated program.



(A). Present and future without capacitors



(B). Future with capacitors

Figure 9. 33-kv customer fed from 33 kv—type 6

This fact emphasizes the need for definite correlation in power-system design groups between those responsible for distribution-system design and those who design and operate the subtransmission, transmission, and generation portions of the system—an over-all viewpoint taken by all of these groups will make for added system economy.

How Far Should Load Kilovars Be Transmitted?

In systems where the loads are relatively near the generating plants, the question may be raised concerning the wisdom of supplying kilovars with capacitors instead of with the generators. The analysis reported in this paper answers

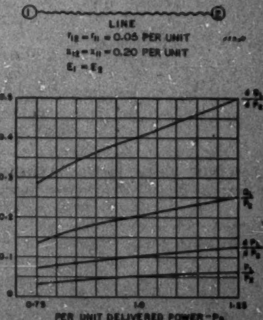


Figure 10. Relation between incremental and actual power losses over a circuit. Voltages maintained equal at points 1 and 2 by kilovar control

this question for a particular system in detail. The following discussion presents a few additional notes from which a quick picture of the general problem may be obtained.

As a basis for discussion and comparison, the data in Table VI are first presented. These figures give the incremental kilovar capacity per kilovolt-ampere required to be built into equipment such as cables, transformers, and generators at constant kilowatt load for certain finite steps in power factor.

On the basis that equipment costs are somewhat proportional to its kilovolt-ampere rating, these figures indicate the degree to which kilovar capacity becomes increasingly expensive at the lower power factors. This is another way of saying the well-known fact that power-factor improvement tends to become uneconomical at the higher power factors.

When Generator Output Is Stepped Up Through Transformers

A unity power-factor load supplied through a 0.30 per unit reactance requires that 1.0 kw +0.30 kilovar be supplied by the generator, corresponding to 0.96 power factor. Following this reasoning, for several load power factors and two values of reactance, gives the data in Table VII.

Since it takes only two transformers to total approximately 0.20 per unit reactance, 0.30 per unit would not be uncommon for the total of a subtransmission circuit where line loading is generally relatively heavy. And with 0.30 per unit it is required that the load power factor be 0.95 if the generator power factor is to be approximately 0.85. Thus, line I^2X losses are not small and are to be respected.

When Load Is Supplied at Generator Voltage

Under this condition for the case of overhead lines, one cannot readily make an analysis in view of the difficulty of establishing the rating and the effect of power factor on the rating of such a line. Purely on the basis of line reactance, however, the data in Table VII are applicable.

Table VIII. Dollars Per Kilovar Investment Chargeable Per 0.05 Step in Power Factor for Three Mileages

Δ Power Factor	4 Miles	8 Miles	12 Miles
1.0 –0.95	1.21	2.42	3.63
0.95–0.9	3.08	6.6	9.24
0.9 –0.85	4.13	8.26	12.39
0.85–0.8	5.2	10.4	15.6
0.80–0.75	6.22	12.4	18.66

At 0.95 power factor the investment chargeable per kilovar capacity for eight miles is $2.42, and the charge for the additional kilovar capacity required in going from 0.95 to 0.9 power factor is $6.18.

Cable circuits, where thermal capacity is the limiting factor, may be approached as follows. A conservative figure for 3/C cable, being operated at the most economical loading, is approximately $1.45 per kilovolt-ampere-mile. An additional charge of about $0.45 per kilovolt-ampere-mile for ducts brings the figure to $1.9 per kilovolt-ampere-mile total. When only these figures are used, and the costs of terminal equipment, such as generators are ignored, the data in Table VIII can be determined readily. These figures give the incremental cost chargeable to incremental kilovar per 0.05 step in power factor for three cable mileages. For other cable lengths costs at a particular power-factor increment are directly proportional to length.

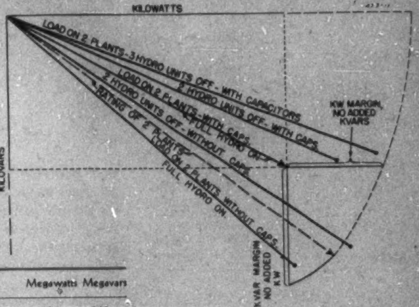


Figure 11. Summary of steam-plant loadings under normal and emergency conditions

Item	Megawatts	Megavars
Rating of two plants	225	169
Load on two plants, two hydroelectric units off		
Without capacitors	235.2	161.6
With capacitors	228.8	101.6
Load on two plants, three hydroelectric units off		
With capacitors	254.3	97.6

These figures give an indication as to how far loads at various power factors can be supplied by cables to balance the investment necessary in capacitors to furnish the incremental kilovars, namely, approximately $5 or $6 per kilovar. For instance, from the table, at four miles, the load power factor should be improved to approximately 0.85, because below this the incremental kilovar costs favor capacitors, and at approximately eight miles the power factor should be 0.95, which somewhat indicates the limit of kilovar transmission through cable.

The kilovolt-ampere loadings of the cables in this discussion correspond approximately to those whose I^2X losses will consume the cable charging kilovolt-amperes. Thus, the cable line-charging kilovolt-ampere is not ignored.

Appendix

Incremental Kilovar and Kilowatt Losses

Case I. Load Voltage Maintained by Raising Sending-End Voltage

This case characterizes a circuit with a regulator or load-ratio control at the substation for maintaining voltage in a remote load area.

$$P_2=\frac{e_2^2\cos\theta_2}{Z_2}=\text{load kilowatts} \qquad (1)$$

where Z_2 = magnitude of load impedance and θ_2 = load power factor. Neglecting line-charging current

$$P_L=\frac{e_2^2Z_1}{Z_2^2}=\text{power loss in } Z_1\text{, the line impedance} \qquad (2)$$

From 1

$$Z_2=\frac{e_2\ \cos\theta_2}{P_2}$$

so

$$P_L=\frac{P^2{}_2Z_1}{e_2^2\cos^2\theta_2} \qquad (3)$$

and the incremental loss with respect to load kilowatts is

$$\frac{dP_L}{dP_2}=\frac{2P_2Z_1}{e_2^2\cos^2\theta_2} \qquad (4)$$

From 3

$$\frac{P_L}{P_2}=\frac{P_2Z_1}{e_2^2\cos^2\theta_2}=\text{actual power loss in per unit of load kilowatts} \qquad (5)$$

If equation 4 is divided by equation 5, the ratio of the incremental to the actual power loss is found to be 2, a well-known result.

This analysis also applies for the load-loss component of transformer and machine losses.

CASE 2. TERMINAL VOLTAGES MAINTAINED BY KILOVAR CONTROL

This case characterizes a subtransmission or transmission circuit utilizing kilovar sources, such as synchronous condensers, at either end for controlling voltage.

The well-known power equations for such a circuit with power flowing from point 1 to point 2 are

$$P_1=\frac{E_1^2}{Z_{11}}\sin\alpha_{11}+\frac{E_1E_2}{Z_{12}}\sin(\delta_{12}-\alpha_{12}) \qquad (6)$$

$$P_2=\frac{E_2^2}{Z_{22}}\sin\alpha_{22}-\frac{E_1E_2}{Z_{12}}\sin(\delta_{12}+\alpha_{12}) \qquad (7)$$

$$Q_1=\frac{-E_1^2}{Z_{11}}\cos\alpha_{11}+\frac{E_1E_2}{Z_{12}}\cos(\delta_{12}-\alpha_{12}) \qquad (8)$$

$$Q_2=\frac{-E_2^2}{Z_{22}}\cos\alpha_{22}+\frac{E_1E_2}{Z_{12}}\cos(\delta_{12}+\alpha_{12}) \qquad (9)$$

P_1, P_2 = real power passing points 1 and 2

Q_1, Q_2 = reactive power passing points 1 and 2

Z_{12} = ratio of voltage at 1 to current at 2 when 2 is grounded

Z_{11} = ratio of voltage at 1 to current at 1 with 2 grounded

Z_{22} = ratio of voltage at 2 to current at 2 with 1 grounded

$\alpha_{11}=\tan^{-1}\frac{r_{11}}{X_{11}} \qquad \alpha_{22}=\tan^{-1}\frac{r_{22}}{X_{22}}$

δ_{12} = angle between voltage E_1 and E_2

$\alpha_{12}=\tan^{-1}\frac{r_{12}}{X_{12}}$

For a line section with no intermediate loads $Z_{11}=Z_{22}$, and when equal voltages held at either end are assumed, the equations for power loss are

$$P_L=\frac{2E^2}{Z_{11}}\sin\alpha_{11}-\frac{2E^2}{Z_{12}}\sin\alpha_{12}\cos\delta_{12} \qquad (10)$$

$$Q_L=\frac{-2E^2}{Z_{11}}\cos\alpha_{11}+\frac{2E^2}{Z_{12}}\cos\alpha_{12}\cos\delta_{12} \qquad (11)$$

It is desired to find the incremental power losses with respect to the receiving power P_2.

Solving for $\cos\delta_{12}$ in equation 7, there is

$$\cos\delta_{12}=\cos(\alpha_{12}+\psi) \qquad (12)$$

$$\psi=\tan^{-1}\frac{\left(P_2-\dfrac{E_1^2\sin\alpha_{11}}{Z_{11}}\right)}{\left[\dfrac{E^2}{Z_{12}}-\left(P_2-\dfrac{E^2}{Z_{11}}\sin\alpha_{11}\right)^2\right]^{1/2}}$$

Substitute equation 12 in equations 10 and 11 and differentiate to get

$$\frac{dP_L}{dP_2}=1-\frac{\cos(\alpha_{12}+\psi)}{\cos\psi} \qquad (13)$$

and

$$\frac{dQ_L}{dP_2}=-\left[\tan\psi+\frac{\sin(\alpha_{12}+\psi)}{\cos\psi}\right]$$

Equations 13 and 14 give the incremental power losses with respect to the delivered power, and, if equations 10 and 11 are divided by P_2, the actual power losses in per unit of delivered power are obtained. A simple relation for the ratio of the incremental to the actual power loss, as was obtained in case 1, is not evident; so, a numerical case is given, as in Figure 10, to enable a concept to be obtained as to their relative magnitudes. From the data in Figure 10 it is seen that the ratio of the incremental to the actual losses is approximately 2 for a large range of values of P_2. This fact, coupled with the factor 2 obtained in case 1, strongly tempts one to draw the general conclusion that no matter how a circuit is operated with respect to holding voltages, the ratio of the incremental to actual power losses in regions of practical operation is always approximately 2.

A Multiplied-Deflection A-C Potentiometer

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THE power required for the operation of an indicating instrument frequently makes its use prohibitive because of the disturbing effect it has on the circuit being measured. If the measurement is in a d-c circuit, the difficulty is overcome readily by using a d-c potentiometer. In a-c measurements, the indicating instruments have a much greater disturbing effect on the circuit, and the use of an a-c potentiometer is very desirable. Furthermore, a-c potentiometers are very useful for measuring power in circuits where the power is too small to be measured with a dynamometer wattmeter.

Most a-c potentiometers have the disadvantage of being too complicated to be used by any but a skilled operator. They have the further disadvantage that they usually are not offered for sale as portable all-purpose instruments. Instead, a special laboratory assembly is used for each task to be performed. One of these arrangements utilizes a calibrated phase shifter. A means of phase detection must be used so that the scale on the phase shifter can be set on the proper value. In addition, the potentiometer current must be standardized for magnitude, and the standardizing process is somewhat more complicated with alternating current than it is in the case of the d-c potentiometer. In another form of the laboratory arrangement, a mutual inductor is used to produce the quadrature component of potentiometer voltage. In this case, the phase angle between two currents must be adjusted to zero, and the magnitude of the potentiometer current must be standardized.

The multiplied-deflection a-c potentiometer eliminates all of these difficulties and makes the operation so simple that an inexperienced operator can make accurate measurements with ease. The instrument is portable and suitable for field work. Very little skill is necessary to make the null balance, and the operator scarcely can make a mistake in determining the results, provided he reads the three indicating instruments (voltmeter, ammeter, and wattmeter) correctly.

The designation *multiplied deflection* was adopted because indicating instruments are used, and their deflections are convenient multiples of the measured quantity. For example, 100 millivolts may be read as 100 volts on a 150-volt indicating instrument, but the reading then is divided by 1,000, as indicated on the potentiometer ratio dial. This potentiometer was developed for the purpose of measuring the impedance and power consumption of circuit elements having a fraction of an ohm of impedance. The current coil of a watt-hour meter was the first circuit element studied, and the results were so highly satisfactory that the instrument was rebuilt with a circuit-changing switch so that it also could be used for measuring high impedances. With this circuit, the impedance and core loss of a doorbell transformer or small radio transformer can be measured easily. High impedances up into the megohms are measured as readily as low impedances, and the circuit may be guarded from leakage in the customary manner, if it is desirable to do so. Numerous other applications become apparent, and some of these will be described later.

Figure 1 shows the scheme used for the measurement of a low-impedance circuit element designated as Z_x. The four terminals (two for current and two for

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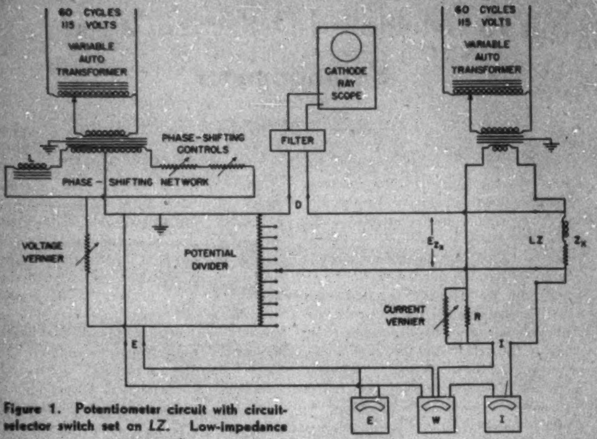

Figure 1. Potentiometer circuit with circuit-selector switch set on *LZ*. Low-impedance measurements

potential) of Z_x are connected to the *LZ* posts. In the upper right-hand part of the figure an adjustable-ratio autotransformer is connected to a two-circuit shielded transformer. The secondary winding provides a current through Z_x in series with an ammeter and the current coil of a wattmeter. This particular potentiometer is designed to provide any current up to ten amperes, but there is no basic reason why a larger current could not be used. The current through the unknown impedance is adjusted to the value required for the test at hand. Major adjustments are made with the autotransformer and minor adjustments are made with the rheostat marked "current vernier." As a result of this current, a voltage drop E_{Z_x} occurs across Z_x.

In the upper left-hand part of Figure 1, another adjustable autotransformer is used to supply voltage to a phase shifter. The phase shifter is a simple uncalibrated network which is capable of having its output voltage shifted through a little over 120 degrees. An iron-core inductor *L* is used in this network, and, in addition to having a functional part in the phase-shifting process, it offers a high reactance to any harmonics that may be introduced by the supply voltage. As a result, the wave form of the phase-shifter output voltage is as good as, or better than, that of the supply voltage. The phase shifter is capable of supplying any output current up to one ampere. This is more current than is needed by its load, which consists of a voltmeter, a wattmeter, and a potential divider, but undesirable voltage regulation during adjustment is thus made negligible. The magnitude of the output voltage of the phase shifter is varied with the adjustable-ratio autotransformer and the voltage-vernier rheostat. Major and vernier phase-shifting rheostats are provided as indicated. A potential divider is used in this circuit in very much the same manner as a volt box is used with a d-c potentiometer. The resistors that make up the potential divider are accurate to one tenth of one per cent and are noninductive.

To obtain a null balance the current through the unknown impedance is first adjusted to the correct value. The voltage (left-hand) autotransformer is then adjusted, until a reading is obtained on the upper part of the voltmeter scale. A 150-volt range is very satisfactory for the voltmeter. The potential divider is then adjusted, point by point, until its output voltage is approximately equal to E_{Z_x}. Exact balance is obtained by adjusting the voltage in both magnitude and phase position, until a zero indication is obtained on a null detector.

Any sensitive detector should be suitable for determining the point of final balance. A cathode-ray oscillograph with a gain of 1,000 in the vertical amplifier is quite satisfactory. Since the oscillograph is rather cumbersome, an electric-eye detector is more suitable for a portable potentiometer. Harmonics occurring in the voltage across Z_x are not balanced by the voltage from the potential divider. These harmonics are eliminated by the use of a filter. It will be shown later that this does not introduce any error.

When the balance has been obtained, the three indicating instruments and the potential-divider ratio *N* are observed. The voltmeter and wattmeter indications are then divided by *N*. The results are calculated from the usual formulas used with voltmeter, ammeter, and wattmeter data. Let *E*, *I*, and *P* indicate the instrument readings. Then

$$E_{Z_x} = E/N$$
$$P_{Z_x} = P/N$$
$$Z_x = E/NI$$
$$R_{Z_x} = P/NI^2$$
$$\cos\theta = P/EI = \text{power factor}$$
$$X_{Z_x} = Z_x \sin\theta$$

As an example of the use of these formulas, assume that the following data are obtained

$E = 90$ volts
$I = 5$ amperes
$P = 200$ watts
$N = 2{,}000$

Then $E_{Z_x} = 90/2{,}000$ volts or 45 millivolts

$$P_{Z_x} = 200/2{,}000 \text{ or } 0.1 \text{ watt}$$

$$Z_x = \frac{0.045}{5} = 0.009 \text{ ohm}$$

$$R_{Z_x} = \frac{0.1}{25} = 0.004 \text{ ohm}$$

$$\cos\theta = \frac{200}{5\times 90} = 0.444$$

$$X_{Z_x} = 0.00807 \text{ ohm}$$

There is no correction for the potential coil or current coil of the wattmeter, because the potential is supplied from a circuit that is independent of the current circuit.

With reference to Figure 3, it may be seen that a switch is provided to change the circuits in the instrument. When the switch is set in the *LZ* position, the circuit shown in Figure 1 is obtained. Figure 2 shows the circuit that results when the switch is set in the *HZ* position. In Figure 2 the potential divider is reconnected so that it becomes a current shunt. The unknown impedance is then attached to the *HZ* terminals and is, therefore, connected in series with the current shunt. A standard 0.2-ohm noninductive resistor R_s is in series with the ammeter and wattmeter current coil, and the drop across R_s is compared with the drop across the current shunt.

In measuring a high impedance the voltage is adjusted to the value desired for the impedance at hand. Actually, the voltmeter reads the voltage across both Z_x and the current shunt. The error caused by this is negligible, since the current shunt is turned to the points of high resistance only when Z_x is exceedingly high. The current-shunt resistors

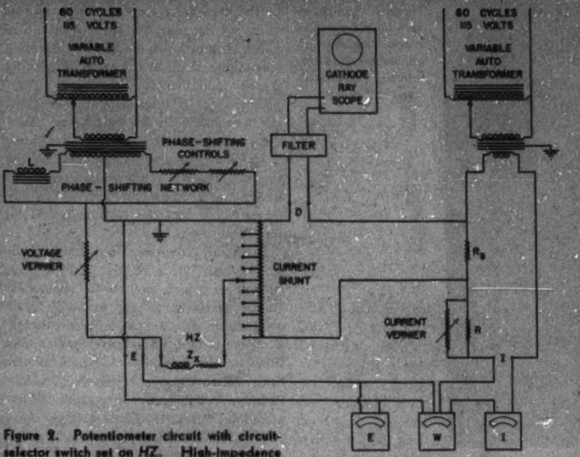

Figure 2. Potentiometer circuit with circuit-selector switch set on *HZ*. High-impedance measurements

are selected so that the error is less than 0.25 volt. The current through R_s is set at any convenient value, which causes the ammeter to read on the upper part of its scale. The current shunt is then adjusted until its voltage drop is equal approximately to the drop across R_s. Final null balance is obtained by means of the current vernier and the potential phase shifter.

The instruments used are the same as those used for measuring a low impedance, and none of the instrument connections are altered. The ratio n is the ratio of the current shunt resistance to the fixed resistor R_s. Two engraved scales are provided on the ratio dial, and care must be taken to read the correct scale. Let E, I, and P indicate the readings of the instrument

$I_{Z_x} = I/n$
$P_{Z_x} = P/n$
$Z_x = En/I$
$R_{Z_x} = Pn/I^2$
$\cos\theta = P/EI =$ power factor
$X_{Z_x} = Z_x \sin\theta$

As an example of a high-impedance measurement, suppose that a small transformer is tested and the following data are obtained:

$E = 115$ volts
$I = 4.5$ amperes
$P = 160$ watts
$n = 1{,}000$
$I_{Z_x} = 4.5/1{,}000$ amperes or 4.5 milliamperes
$P_{Z_x} = 160/1{,}000$ watts or 160 milliwatts
$Z_x = 115{,}000/4.5$ or 25,600 ohms

$$R_{Z_x} = \frac{160{,}000}{20.2} \text{ or } 7{,}930 \text{ ohms}$$

$$\cos\theta = \frac{160}{115 \times 4.5} = 0.31$$

$$X_{Z_x} = 25{,}600 \times 0.95 = 24{,}300 \text{ ohms}$$

In Figure 3 the two adjustable-ratio autotransformers are not shown. These are attached as auxiliary equipment. The ammeter and wattmeter are connected to the posts marked *I*. The voltmeter and wattmeter are connected to the posts marked *E*. High-impedance circuit elements are attached to the two posts marked *HZ*, and low-impedance circuit elements are attached to the four posts marked *LZ*. The detector is connected to the posts marked *D*. The ventilated compartment, mounted in the back, houses the phase-shifting network. Three fuses (upper center) protect the resistors in the current shunt when the circuit of Figure 2 is used. The fuses are inserted in such a way that the fuse resistance does not affect the calibration.

Miscellaneous Applications

To measure a low voltage it is necessary to connect it only across the two inner posts of the *LZ* terminals. The circuit selector is set on *LZ*, and the balance is made without switching on the current circuit. No current is drawn from the unknown potential at balance, and the voltage is determined by reading the voltmeter and dividing by the potential-divider ratio.

To calibrate a millivoltmeter when only a standard 150-volt voltmeter is available, the standard voltmeter is connected to the *E* posts of the potentiometer. The millivoltmeter may be operated from an independent source, and the values may be measured by connecting from the millivoltmeter to the inner posts of the *LZ* terminals.

To calibrate a milliammeter when only a standard five-ampere instrument is available, it is necessary only to insert a rheostat with the milliammeter in series across the *HZ* terminals and to connect the standard ammeter to the potentiometer–ammeter terminals.

To measure leading power-factor elements of either high or low impedance, or to measure the capacitance and power factor of a capacitor, it is necessary to make a change in the range of the phase shifter. As previously stated, the phase shifter covers a range somewhat greater than 120 degrees. If a three-phase supply is available, the left-hand autotransformer can be connected to either phase, and the entire 360-degree range may be utilized. The phase shifter in the potentiometer described covers the range from in phase to 120 degrees lagging when both autotransformers are connected to the same phase. If it is desirable to measure leading power-factor elements in a location where three-phase power is not available, the construction of the phase shifter should be altered so that the inductor *L* is replaced by a capacitor. The phase shifter will then cover the range from in phase to approximately 180 degrees leading. The output voltage of the phase shifter then should be filtered to eliminate any objectionable harmonics that are amplified by the capacitor.

The impedance and power loss due to leakage and dielectric hysteresis in lead-covered cables can be measured in the same manner as a capacitor. If it is desirable to guard the insulated ends of the cable, the guard wire should be attached to ground.

The application for which this instrument was originally designed is that of measuring the burden occurring on a current transformer in a metering installation. Since the potentiometer is portable, the burden may be measured at the site of the installation simply by disconnecting the current transformer. The transformer then may be removed and calibrated in the laboratory, if a duplicate burden is provided.

Sources of Error

Since the data on the multiplied-deflection potentiometer are obtained from indicating instruments, the entire pro-

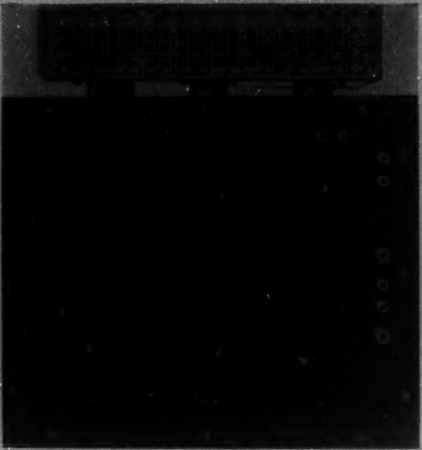

Figure 3. Top panel view of the potentiometer, exclusive of adjustable-ratio transformers, instruments, and null detector

cedure is limited in accuracy to the accuracy of these instruments. If high-grade dynamometers are used, an overall accuracy of one quarter of one per cent can be obtained. Errors in the resistors used in the potentiometer are reduced so that they are negligible compared with the instrument errors. Insulation resistance within the potentiometer must be arranged carefully to prevent leakage across the *HZ* terminals. Since the high-impedance values sometimes run into the megohm range, the insulation resistance must be in the hundreds of megohms in order to realize maximum accuracy. If desirable, these leakage paths may all be eliminated by guarding in the conventional manner. All guards should be attached to ground.

It should be noted that the transformer in the current circuit is shielded and grounded. A similarly shielded transformer is incorporated in the phase-shifting network. This precaution is necessary in order to prevent capacitance currents from feeding through the potentiometer from one autotransformer to the other.

The most irritating question pertaining to the accuracy of the instrument has to do with the handling of harmonics. This problem is not peculiar to this particular potentiometer. In fact, the problem is not a matter of potentiometer accuracy, but a question of just what is meant by the impedance and power factor of an iron-core coil which generates harmonics when a sine wave of current is passed through it.

With reference to Figure 1, suppose that Z_x is an iron-core reactor. The current passed through it is of good wave form, because the resistor R is the chief impedance in the circuit. The voltage drop across Z_x probably will contain conspicuous harmonics. The voltage output from the voltage divider has a very good wave form. Thus, it may be seen that the output voltage from the voltage divider is adjusted actually to equal the fundamental component of voltage drop across Z_x, and the harmonics are eliminated by the filter. Under this condition, the result obtained by dividing the voltmeter reading by N is the rms value of the fundamental component of the impedance drop, and not the rms value of the actual voltage drop. The value of impedance obtained by dividing this voltage drop by the ammeter reading is the fundamental component of impedance. It is interesting to note that the harmonics do not cause any doubt about the wattmeter accuracy, for, even if the harmonics were impressed across the potential coil of the wattmeter, there would be no harmonic current in the current coil, and the reading would be the same.

In Figure 2 suppose that Z_x is a small transformer, the core loss and exciting current of which are to be measured. In this case, the voltage across Z_x is of good wave form, but the current through Z_x will contain pronounced harmonics. When the potentiometer is balanced, the harmonics are again eliminated by the filter, and the value of current determined by the potentiometer is the rms value of the fundamental component of exciting current. The calculated impedance is the fundamental component of impedance. The divided wattmeter reading is the true value of power loss.

Operation at Higher Frequencies

The design of the multiplied-deflection potentiometer is made possible because the dynamometer wattmeter provides isolation between its potential coil and its current coil. Thus, the range of frequency is the same as that of a dynamometer wattmeter. This places the upper limit at about 1,000 cycles. A modified design, which utilizes a thermocouple wattmeter, is being studied, but this calls for major changes in the entire circuit design.

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Automatic Ticketing of Telephone Calls

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MEMBER AIEE

THERE has recently been placed in commercial service in the Bell System a new arrangement of telephone switching equipment designed to enable telephone users in a metropolitan area to dial for themselves their calls to nearby points instead of placing them with an operator. This added central-office equipment has the distinctive feature that it automatically prepares an individual printed ticket showing the information needed for charging for the call. The initial installation serves some 8,000 subscribers at Culver City, adjoining Los Angeles. It enables them to dial directly, in the same way as local calls, their calls to various points within the Los Angeles metropolitan area and thereby enlarges the scope of their dial service, with its attendant advantages in speed and convenience.

This paper describes briefly the functions and design of this form of automatic ticketing, which is designed for use with central-office switching equipment of the step-by-step type. In addition to ascertaining and printing the information needed for billing, the equipment also incorporates features for routing calls economically through the trunking network. The plan in some respects resembles and in other respects differs from automatic ticketing arrangements developed abroad and used commercially in several networks in Belgium prior to the war.[1]

Present Methods of Handling Calls

Before describing the operation of the new system it may be well to review the present methods of handling telephone calls in metropolitan areas. The methods outlined below are, in essence, those used at Los Angeles, the largest city in the United States employing step-by-step equipment.

Figure 1 is an idealized map of a metropolitan area. The heavy circle represents the boundary of the city exchange; in general, it includes the closely built-up business, industrial, and residential regions, but is not identical with the municipal boundary. The city exchange may be divided into zones, shown dotted, used for determining toll rates for calls between various parts of the city and the several suburbs, and in some cases between the zones of the city itself. Surrounding the city are nearby suburban exchanges designated *A* to *H;* these may be well-separated communities, or may practically merge with each other or with the city. Farther out are other suburbs designated *M* to *T*. While such geographical regularity is not to be found in any actual city, this picture will serve to illustrate principles.

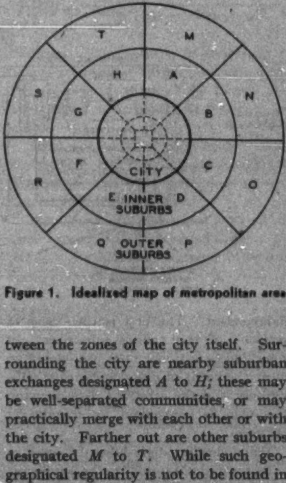

Figure 1. Idealized map of metropolitan area

The lines serving telephone subscribers terminate in central-office buildings located in the various exchanges. The city exchange has a number of buildings, some of which may house as many as five or six central-office switching units, the conventional central office having a capacity of 10,000 subscriber numbers. A suburban exchange may have one or several buildings each with one or a few central offices.

The switching plan for completing calls between subscribers in such a metropolitan area may be visualized by considering calls originating in a specific central office, for example, one in the suburban exchange designated *F*. Figure 2 shows in simplified form the switching plan of such an office. The subscriber lines are connected to dial switching equipment, which may be regarded as consisting of two parts designated "Originating" and "Terminating." The originating portion consists of the switching paths by which a call is routed to interoffice trunks. The terminating portion consists of the switching paths by which a call terminating in this office reaches the called subscriber.

Of the calls originated by a subscriber in this office the great majority will ordinarily be short-distance calls within what may be termed his local charge area, that is, the area within which his call is counted as one message unit if he subscribes to message-rate service, or is not counted if he pays for flat-rate service. This area may consist simply of his own exchange or may include certain nearby exchanges or zones. He dials these calls directly, if the area has been fully converted to dial operation. Such calls, in the typical case, go over direct trunks to the terminating office as illustrated at the lower left of Figure 2.

On calls to the more distant exchanges, for which there is a toll charge or a charge of more than one message unit, the subscriber dials "O" and passes his call to an operator. The switchboard provided for handling this and certain other types of traffic is included in Figure 2. The operator may complete the call by one of several methods, of which two are illustrated. If the called office is one for which there is enough traffic to warrant direct trunks, the operator extends the call over a direct trunk to that office and dials the last four digits of the called number. Many of the distant offices, however, receive too little traffic from this office to warrant an individual group of direct trunks and are reached through tandem switching equipment located at

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some central point in the area.[3] The operator extends such a call over a trunk to the tandem office and dials the complete number, thereby causing the tandem-office switches to select a tandem-completing trunk to the called office and the switches in that office to complete the connection. Whatever the method of completion, she writes a ticket showing the information needed for charging for the call.

Subscriber Dialing of Ticketed Calls

As previously stated, it is the purpose of the automatic ticketing equipment to make it unnecessary for the subscriber to place such calls with the operator but, instead, to permit him to dial them in the same way that he now dials local calls.

Figure 3 shows in block form the arrangement of switching equipment in the originating office when automatic ticketing of the type to be described has been added. In comparison with Figure 2, the amount of originating switching equipment is increased somewhat to provide paths for the added calls completed by dialing. The switchboard correspondingly is reduced. The direct and tandem trunks carrying ticketed calls, formerly appearing at the switchboard, are made accessible to the originating switches through added equipment which performs the two functions labeled "Routing" and "Recording."

The recording function presents the more novel features and will be considered first. The problems associated with it may be brought out by considering the form of record which the equipment is to produce.

Form of Ticket

Figure 4 shows a mechanically prepared ticket and a ticket of the type now written by the operator. On the operator's ticket the entry below the date is the calling party's number, ARdmore 8-2345, given by the subscriber at the time he places his call. Below are the called number and the times of end and start of the conversation period. The operator recognizes the answer of the called subscriber and the disconnect of both subscribers by observing the supervisory lamps of the cord circuit used for completing the connection and reads the corresponding times from a clock at her position whose dials show the hour, minute, and tenth of minute. The ticket provides space for entering later the conversation time, obtained by subtraction.

The automatically prepared ticket contains essentially the same information, shown, however, entirely as numerals in order to permit using a simple type of printing mechanism. The first entry is the calling number, the two letters of the office name being shown in terms of their numerical equivalents as they appear on the dial. The following group of seven digits shows to the tenth of an hour the time at which the call was made. Next is a group of three digits provided as an aid to maintenance; it shows which of certain units of common equipment were involved in the handling of the call. The called number is shown similarly to the calling number.

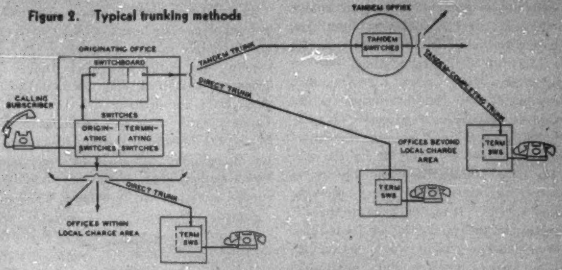
The following two digits, though printed together for space reasons, are independent. The first is the "class of service" of the calling subscriber. In some offices optional rates are offered to meet the needs of different types of subscribers, and the arbitrary digit showing this class is convenient for accounting purposes.

The following digit labeled "message units for initial interval" is the significant entry with regard to the charge for the call. The digit 3, for example, may represent a toll charge of 15 cents, or the addition of three message units to the subscriber's message usage. The equipment determines this figure from the destination of the call and the rate schedule of the calling subscriber. The figure applies to calls which do not extend beyond the initial five- or three-minute interval; on the relatively small percentage of overtime calls the overtime charge is subsequently added as an accounting procedure.

The information up to this point is printed on the ticket immediately after the subscriber dials his call. When the called subscriber answers, timing is started under control of a synchronous timer. When the subscribers hang up, the elapsed whole and fractional minutes are printed as minutes of conversation time. The time is normally shown as a two-digit number, but provision is made for printing additional digits for the rare calls that exceed 99 minutes.

On a call which is not completed because of encountering a "busy" or "don't answer," or for any other reason, no conversation time is printed and the resultant short ticket is eliminated in the accounting process.

Figure 2. Typical trunking methods



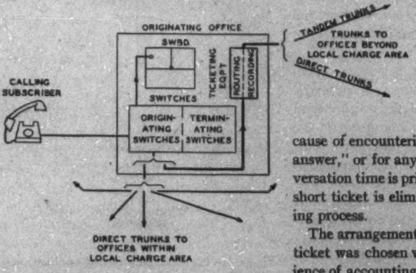


Figure 3. Trunking with automatic ticketing

The arrangement of information on the ticket was chosen with regard to convenience of accounting procedures and minimizing of holding time of the various circuits involved in providing the information. Its size is that of the standard toll ticket written at long-distance switchboards and was chosen for convenience in handling and filing as well as to provide space for use with future mechanical accounting processes referred to later.

Message Ticketer

Figure 5 shows photographs of the machine, designated a "message ticketer," which prints and cuts the ticket. Power for rotating the type wheel is supplied by a small a-c motor through a friction clutch. An electromagnet controlled through a commutator stops the type wheel, presses the paper against the character to be printed, and on its release steps the paper forward. A second magnet operates the knife. Mechanical simplicity is obtained by vesting control in external circuits connected to the ticketer when required through some 20 connecting leads. The printing speed averages about seven characters per second.

Functional Arrangement of Ticketing Equipment

The relationship of the major circuits of the ticketing system, one of which includes the message ticketer, is shown by the functional diagram, Figure 6. Each line of this diagram represents the total conductors comprising a connecting path; the path through the selectors normally consists of three conductors (the two wires of the talking circuit and a control wire), but other paths may consist of as many as 100 or more conductors. The symbols above the broken line represent, in very condensed form, the existing switching equipment of an office, while those below represent the major components of the equipment added for automatic ticketing.

The selectors shown above the broken line form part of the originating equipment of Figure 3. They consist of first, second, and third selectors which respond to the initial two or three digits of the called subscriber's number; these digits, which identify the office being called, are termed the office code. Each office corresponds to a level of these selectors. The levels corresponding to offices within the local charge area are shown connected to direct trunks leading to the respective offices. The levels corresponding to offices for which calls are to be ticketed are connected to ticketing trunk circuits and thence to the direct or tandem trunks over which the calls are to be completed.

Each trunk circuit consists of a group of relays and other telephone apparatus together with a message ticketer.

Associated with the trunk circuits there is provided a common group of senders whose functions will be described later. A sender is associated with a trunk circuit during the setting up of the call by way of an eight-wire step-by-step switch used as a trunk finder. Each sender consists of some 200 relays and other telephone apparatus used for controlling switching and recording operations.

The senders in turn have access to two or three "identifiers" which are the heart of the ticketing system. The identifier has the function of ascertaining the calling number and other equally important functions having to do with the routing of the call. It consists of 400 to 500 relays, a number of vacuum-tube circuits, and various other apparatus.

For ascertaining the calling number the identifier requires access to terminals corresponding to the numbers of all subscribers whose lines terminate in the building and who have access to the ticketing equipment. These terminals appear on the "thousand-number" circuits, of which there is one for each thousand consecutive numbers in the building.

Other paths for interconnecting various circuits for short periods during the establishment of a call are indicated on the diagram. They consist of multicontact relays which close through various numbers of wires as required for control and interchange of information between circuits.

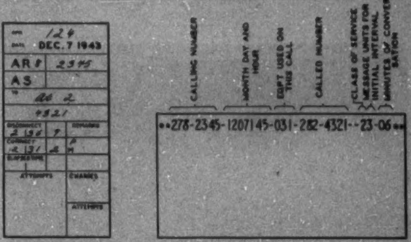


Figure 4. Tickets

OPERATOR'S TICKET

MECHANICALLY PREPARED TICKET

"Day-and-hour" circuits connected to the senders control printing of the time of placing the call. Various minor circuits are omitted for simplicity.

The figures in circles show the quantities of the major circuits installed at Culver City for handling between 5,000 and 6,000 ticketed calls daily.

Method of Operation

The general operation of the system may now be traced. A subscriber making a call to a point for which a ticket is required dials the called number as listed in the directory. Dialing of the office code digits causes the call to advance through the selectors as on a local call, and to reach a level corresponding to the called office. This level, representing a destination for which calls are to be ticketed, is wired to a ticketing trunk circuit.

The trunk circuit causes a trunk finder to attach a sender, in which the further digits dialed by the subscriber are temporarily stored. The sender in turn calls in an identifier which performs two major functions. It provides the sender with information for routing the call, whereupon the sender sends out over the trunk the necessary pulses for setting up the call through distant switches. Concurrently, the identifier ascertains the calling number and furnishes the information to the sender. The sender, as an overlap with setting up the call, causes the message ticketer in the trunk circuit to print the initial information on the ticket. The identifier and sender are released when their respective functions have been performed.

When the called subscriber answers, apparatus in the trunk circuit starts timing. Finally, when the subscribers disconnect, the trunk circuit causes its ticketer to print the conversation time, after which it is freed for the next call.

Calling-Number Identification

Identification of the calling number presents some features of interest. As shown in Figure 6, the identifier has access, through a multicontact relay connecting path, to the trunk circuit at which the call has arrived. It has access to all the thousand-number circuits through similar paths. The principle is simple. The identifier sends into the trunk circuit a signal which finds its way back through the selectors to the terminal associated with the calling subscriber's number in one of the thousand-number circuits. While sending the signal it rapidly scans these circuits, determining successively the thousand, hundred, ten, and unit digits of the terminal on which the signal appears. These digits are transferred to the sender, which controls their printing on the ticket.

Figure 7 shows the process in more detail. The signal used is an a-c potential of about five volts at 270 cycles, impressed by the identifier on the control wire of the path through the selectors; this signal does not interfere with the normal functions of this wire, nor affect the talking circuit. Connections of the control wires of the subscriber-line equipments to the thousand-number circuits are taken from terminals associated with the connector switches through which subscribers receive incoming calls and which therefore determine their respective numbers. These single-wire connections are cabled in numerical sequence to the thousand-number circuits, where each terminates in one contact of a multicontact relay and also in a capacitance–resistance network. One hundred consecutive networks may be connected in parallel to a "hundred" coil, and ten such coils are connected to a "thousand" coil. Therefore, when the a-c potential is applied to the control wire of the trunk, it follows the control wire back through the switches to the calling line and causes a signal to appear in the output winding of the appropriate "hundred" coil and "thousand" coil.

Each identifier has ten amplifier–detectors which it can connect through the thousand-number connectors to ten "thousand" coils. Finding a signal in one of them, it registers the digit designating that thousand on a group of register relays labeled *TH*. Through relay operations it then transfers its detectors to the ten "hundred" coils of that thousand. The hundred having been found and registered on the *H* register, the identifier operates the proper multicontact relays of the thousand-number circuit to connect those 100 terminals directly into the identifier. There it first groups them by tens and finally looks at the individual terminals of the ten, registering the ten and unit digits. The record of the number is transferred as received into the sender over a multilead connecting channel.

The foregoing describes identification within a central-office unit of 10,000 numbers. In buildings having more than one central office, the identifier first scans the central offices in succession, 10,000 or 20,000 numbers at a time, a second set of ten detectors being provided where the load warrants. The complete identification time averages less than two seconds.

The frequency of 270 cycles was chosen after a survey in working offices of extraneous potentials on control wires. The time and method of applying the detectors take account of the transients due to switching operations. Interlocks are provided to avoid conflicts when two or more identifiers are proceeding simultaneously. Check tests indicate irregularities due to wiring faults and provide second trials to overcome any contact irregularities.

Additional features are provided for differentiating between the two subscribers on two-party lines. These involve arrangements at the stations to produce different electrical conditions on the line, depending on which station is making the call, and a test of the line by the central-office apparatus. Facilities are not now being provided for differentiating between the stations on four-party or on rural lines, and calls from such stations will continue to go by way of the operator. Means are provided for preventing readily completion of calls through ticketing paths from these or other stations which for any reason are not to have direct dialing, without the necessity of segregating their lines in the switching equipment.

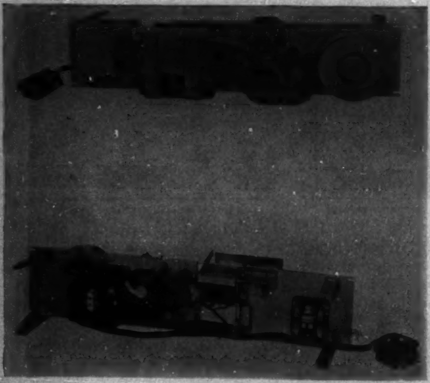


Figure 5. Message ticketer (front and rear views)

Class Identification

The a-c marking signal on the control wire of the switching path is used also for "class identification," which is not shown in Figure 7, in view of its similarity with the first stage of number identification. In a telephone exchange various classes of service may be offered, such as individual and party-line service with flat or message rates, or optional rate schedules differing in the extent of the local charge area. Some of these classes may require different types of equipment associated with the first selectors, or access to different paths reached by the selectors, and are accordingly segregated into appropriate line groups having access to corresponding groups of first selectors.

To enable the identifier to distinguish between classes, first selectors have networks of the type mentioned earlier associated with their control wires, and these are connected by selector groups to the proper ones of a few "class" coils. When a connection has been set up, this a-c path is therefore in parallel with that associated with the subscriber number. While the identifier is making number identification, other detectors are connected simultaneously to the class coils. The class indication thus ascertained is used for governing circuit functions and for printing the class on the ticket.

Routing Functions

As indicated in Figure 3, the second principal function of the ticketing equipment is to govern the routing of the call. This problem arises because of an inherent limitation of the simple step-by-step system with regard to the handling of calls through a comprehensive tandem system. Limited forms of tandem operation are possible through judicious choice of the numbering plan, and in fact such arrangements are extensively used in the Los Angeles network. The following, however, will indicate the nature of the problem of handling calls through a centralized tandem office such as was indicated in Figure 2.

Assume that the subscriber is calling a distant office reached through tandem and has dialed the office code digits of the number. These digits have controlled directly the various selectors and have caused the call to arrive at a selector level as indicated in Figure 6. The selector level is connected to a trunk terminating on tandem-office switches which must be supplied with the digits required

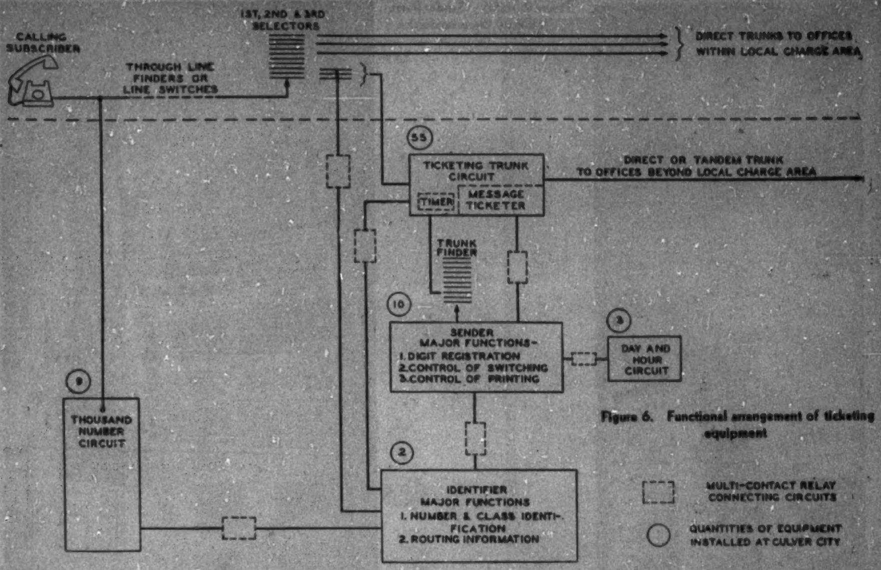

CALLING SUBSCRIBER
THROUGH LINE FINDERS OR LINE SWITCHES
1ST, 2ND & 3RD SELECTORS
DIRECT TRUNKS TO OFFICES WITHIN LOCAL CHARGE AREA
55
TICKETING TRUNK CIRCUIT
TIMER
MESSAGE TICKETER
DIRECT OR TANDEM TRUNK TO OFFICES BEYOND LOCAL CHARGE AREA
TRUNK FINDER
10
SENDER MAJOR FUNCTIONS-
1. DIGIT REGISTRATION
2. CONTROL OF SWITCHING
3. CONTROL OF PRINTING
3
DAY AND HOUR CIRCUIT
9
THOUSAND NUMBER CIRCUIT
2
IDENTIFIER MAJOR FUNCTIONS
1. NUMBER & CLASS IDENTIFICATION
2. ROUTING INFORMATION
MULTI-CONTACT RELAY CONNECTING CIRCUITS
QUANTITIES OF EQUIPMENT INSTALLED AT CULVER CITY

Figure 6. Functional arrangement of ticketing equipment

for directing the call to the terminating office; but these are the digits that already have been used up in the selectors of the originating office. The equipment associated with the ticketing trunks must, therefore, be capable of reconstructing these digits, meanwhile storing the additional digits being dialed by the subscriber and repeating them after the call has been extended to the terminating office.

It should be noted further that, in the originating office, the traffic to the terminating office we are considering is combined with the traffic to numerous other terminating offices over the common group of tandem trunks and that accordingly a given ticketing trunk may be reached from a number of selector levels. The equipment, therefore, has the problem of ascertaining the selector level over which a given call has reached the trunk,

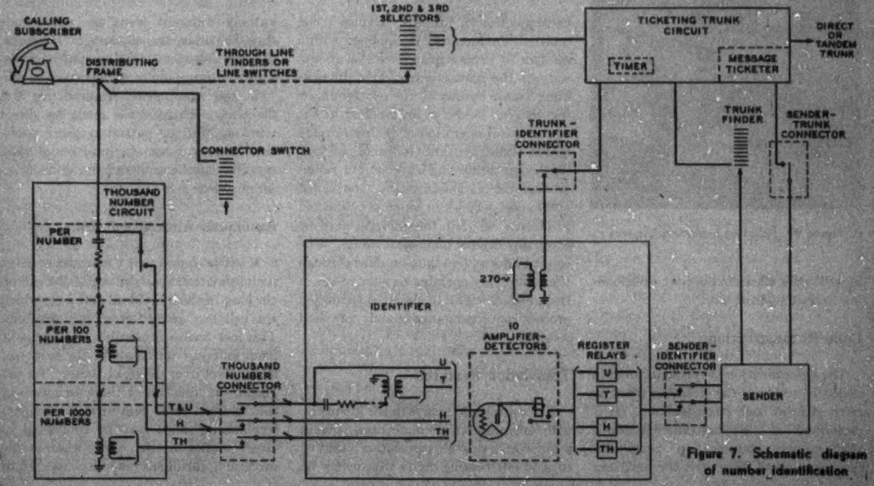

CALLING SUBSCRIBER
DISTRIBUTING FRAME
THROUGH LINE FINDERS OR LINE SWITCHES
1ST, 2ND & 3RD SELECTORS
TICKETING TRUNK CIRCUIT
TIMER
MESSAGE TICKETER
DIRECT OR TANDEM TRUNK
CONNECTOR SWITCH
TRUNK-IDENTIFIER CONNECTOR
TRUNK FINDER
SENDER-TRUNK CONNECTOR
THOUSAND NUMBER CIRCUIT
PER NUMBER
PER 100 NUMBERS
PER 1000 NUMBERS
T&U
H
TH
THOUSAND NUMBER CONNECTOR
IDENTIFIER
270~
10 AMPLIFIER-DETECTORS
U
T
H
TH
REGISTER RELAYS
U
T
H
TH
SENDER-IDENTIFIER CONNECTOR
SENDER

Figure 7. Schematic diagram of number identification

Figure 8. Ticketing trunk-circuit frames

Figure 9 (right). Sender frame (capacity three senders)

as well as the office code digits corresponding to that selector level.

Code Reconstruction

The determination of the digits already dialed in reaching the ticketing trunk is referred to as code reconstruction. It is performed by somewhat different means than number identification, partly for reasons of speed, since it is the first step in completion of the call. Details being omitted, the identifier is equipped with a number of "route relays," which it presents to the various entrances of the ticketing trunk by way of the relay connecting circuit. A d-c signal applied to the trunk causes operation of the route relay corresponding to the entrance in use. The route relay carries contacts representing the various digits of the called office code, which can be cross-connected arbitrarily so that these digits may be given any desired numerical values. The routing information thus supplied through the route relay operates register relays in the sender. The code reconstruction process requires approximately one-half second.

Translation Features

The use of route relays in the identifier, with arbitrary cross-connections, makes it possible to provide translation; that is, to send out routing digits which may be entirely different from the office code digits dialed by the subscriber. However, in areas for which this equipment is designed, it is generally unnecessary to provide the equipment required for full flexibility. Provision is made for direct trunking, full or partial tandem operation, and the automatic insertion of voice repeaters at the tandem office on calls requiring their use.

Automatic Alternate Routing

It will be noted that the sender receives its information as to routing of the call on the basis of the individual path over which the call has arrived from the selectors. This fact makes it possible to route calls from different terminals of the same selector level over different routes; for example, the first two or three terminals of the selector level representing a given terminating office may be connected to direct trunks to that office, whereas the succeeding terminals may be connected to

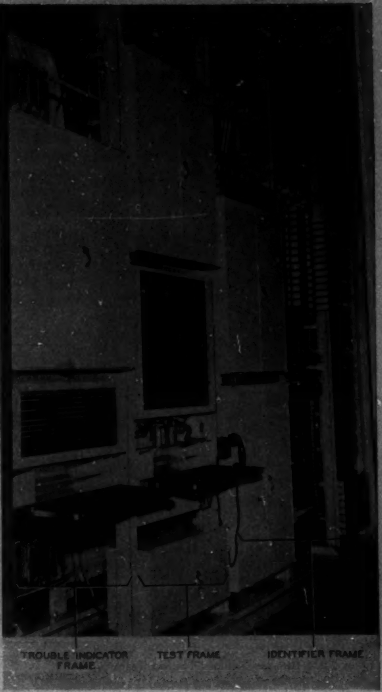


Figure 10. Trouble-indicator frame, test frame, and identifier frame

trunks to tandem. Calls which find the direct trunks busy therefore will overflow into the tandem trunks. The equipment will provide in each case the appropriate routing for reaching the same terminating office. This process is known as automatic alternate routing. It affords material economy in the design of the trunk plant because it permits provision of a small number of direct trunks used at high efficiency and combines the overflow traffic with that to other offices over the common tandem trunk group, also used at good efficiency.

Nonticketed Calls

There are cases where economy in trunk plant can be gained by taking advantage of the trunking features of the ticketing equipment and routing through it certain calls dialed by subscribers or operators for which no ticket need be printed. The equipment is arranged, under control of the class indication, to omit the ticket on such calls, and if the call is from a message-rate subscriber, to cause operation of the message register instead. Trunk circuits which omit the message ticketer entirely may be provided where economical.

Equipment Arrangements

Except for the message ticketer and an arrangement to facilitate cross-connection changes in the thousand-number circuit, the ticketing system employs no essentially new types of apparatus. The relatively complicated functions are performed by suitable interconnection of relays, small electromechanical switches, vacuum-tube circuits, and other apparatus of the types commonly employed in dial switching systems.

The frames carrying the ticketing trunk equipment are arranged to bring the five ticketers of the frame together at a convenient height as shown in Figure 8. Figures 9, 10, and 11, respectively, show photographs of sender frames, an identifier frame with the adjacent test and trouble indicator frames referred to later, and a group of thousand-number frames, as installed at Culver City.

Maintenance Features

The sender and identifier circuits are of considerably greater complexity than have heretofore been used in step-by-step switching systems. A test circuit accordingly is provided, capable of verifying numerous features for routine tests or for locating indicated trouble. In addition, there is provided a "trouble indicator" patterned after that used in the crossbar switching system. It includes a panel with numerous lamps indicating various stages of the progress of the call. It is brought into play when irregularities are detected in the operation of the ticketing circuits, and the record remains even though the second trial features may cause the call to be completed satisfactorily. The information assists the maintenance force in clearing out minor troubles before they become serious and major troubles with minimum reaction on service.

General Design Considerations

With the foregoing description of the equipment in mind, some of the general considerations entering into its design may be pointed out.

In providing direct dialing to the more distant points of a metropolitan area, consideration was given to the automatic ticketing method in comparison with another method known as zone registration. This plan, which has been used to some extent in other dial systems, consists in operating the subscriber's message register more than once on completion of a call to a distant zone in accordance with the rate schedule, and on overtime calls to operate it further for each overtime interval. One important consideration in the Los Angeles area, however, was that the percentage of subscribers having flat-rate local service is relatively high, particularly in the suburbs, and for these subscribers it would have been necessary to add message registers used only for recording the multiunit calls. In addition, there were circuit problems in arranging the existing registers for repeated operation. Aside from technical considerations the individual record provided by the automatic ticketing method has commercial advantages which increase in importance as the distance, and consequently the charge for the call, increase.

The ticketing system has been described on the basis that a subscriber,

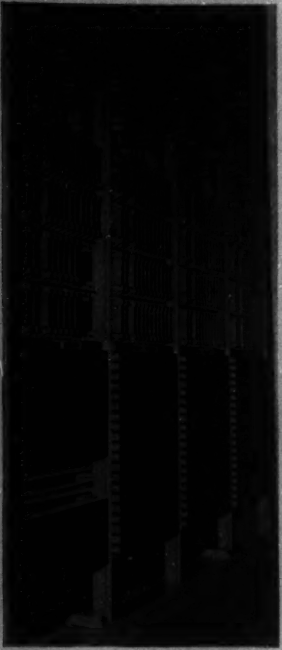


Figure 11. Thousand-number frames (capacity 2,000 numbers per frame)

calling from anywhere in the metropolitan area, will dial the number as listed in the directory to reach any other subscriber in the area. This arrangement, known as a universal numbering plan, has been adopted as the ultimate plan for exchanges included in the Los Angeles metropolitan area, now embracing some 975,000 telephones. This plan, in combination with automatic ticketing, will give the subscriber a uniform method of dialing his calls, as distinguished from the use of directing codes to be prefixed to the number on calls between different exchanges of the area.

The choice of a numbering plan for any area is, however, a telephone problem in itself, and it only need be pointed out here that the ticketing arrangement has capabilities for being used under various plans. Its use on calls between different cities, in cases where the traffic may be sufficient to warrant, would require, in general, some form of directing codes and suitable instructions to subscribers. The ticketing plan can be adapted to such arrangements as further experience may show to be desirable.

Ease of application to existing offices was a primary factor in design. At Los Angeles all the city offices have been converted to dial operation as have also the majority of the suburbs. The ticketing equipment can be added to offices having either line finders or line switches with only minor modifications of a few of the existing circuits.

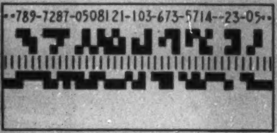


Figure 12. Ticket arranged for photoelectric scanning

The technical problems of handling ticketed calls are in many ways similar to those encountered in handling traffic in large metropolitan areas in which crossbar equipment is now used.[3] The techniques employed in the ticketing system have taken advantage of the developments and experience gained with the crossbar system. Some of these techniques may be pointed out.

The concentration of complicated functions in a few common circuits has various advantages but requires high-speed operation and special safeguards. High speed is obtained by the use of simple quick-acting apparatus, chiefly relays of ordinary telephone types, and the liberal use of wires for interconnection between circuits within the building. Connection is made through multicontact relays which are carefully interlocked.

Various safeguards are built into the circuits for detecting irregularities. To illustrate, registration of a digit which may have any numerical value from zero to nine is commonly done over a group of five wires with combinations of two closures out of five. The receiving relays are arranged so that if fewer or more than two closures are received, as may occur in case of contact failure or a wiring cross, the condition is instantly recognized. Where possible, the circuit makes a second trial, using other paths if available. In any case the irregularity is registered on the trouble indicator as an aid in preventive maintenance.

Mechanical Accounting

The design of the system provides for future application of mechanical methods to the accounting processes. A possible method is to arrange the ticketer so that, in addition to the digits which it now prints, it will print also their equivalents in code marks which can be recognized by photoelectric scanning. A ticket so arranged is shown in Figure 12.

Other forms of record produced by different types of recording mechanisms may be found advantageous, and the equipment for use at the accounting center may be designed to mechanize the accounting processes to any degree found economical.

Conclusion

Further application of ticketing equipment will, of course, have to await the end of the war. In the meantime the limited installation which the Southern California Telephone Company has been able to make at Culver City will provide useful experience and a basis for further development in this phase of the telephone art.

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Fusing Practices on Distribution Systems

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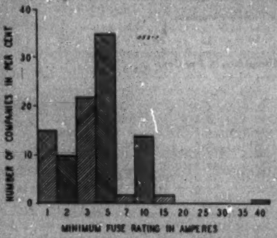

Figure 1. Minimum fuse rating employed by 167 companies

100 per cent = 167 companies

DISCUSSION of fusing practices on distribution systems with numerous operating engineers gives the impression that there are almost as many practices as there are operating companies. To determine present fusing practices in this country, a questionnaire was submitted to some 200 operating companies, and replies were received from 168, indicating a real interest in this subject. The data presented here apply only to systems operating below five kilovolts and were obtained from companies located in all parts of the country. All fuse link data are given on the basis of *N* rated links. The small amount of data which was received involving 65 per cent rated links has been converted to equivalent *N* rating. No attempt is made to weigh the data by taking into consideration the size of the operating company, as it is the intention to show engineering thought on the subject.

Distribution-Transformer Fusing

In order better to understand the data obtained from the questionnaire, the first question asked was, "Why do you fuse distribution transformers?" The replies show that 80 per cent of the companies fuse to protect the system from the transformer, 59 per cent fuse to protect the transformer, 17 per cent use fuses to indicate the location of faults, and 16 per cent use them as an isolating means. While a number of companies gave only one reason for fusing transformers, others gave two or more. Thus, as these percentages are based on the number of companies, the total will be more than 100 per cent.

Table I. Operating Companies Using Various Combinations of Transformer and Fuse Ratings

2,400 Volts Single-Phase or 4,160 Volts Three-Phase Wye
100 Per Cent = 144 Companies

Single-Phase Transformer Rating—Kva (Multiply by 3 for Bank Kva)	Fuse Link Ratings—Amperes														
	1	2	3	5	7	10	15	20	25	30	40	50	65	85	100
	Number of Companies in Per Cent														
1.5	4.1	22.8	18.7	35.0	1.6	15.4	2.4								
3		2.1	44.8	33.8	2.1	13.8	3.4								
5			3.3	64.5	4.6	21.7	5.9								
7.5			0.7	9.4	25.4	53.6	8.1	1.4	1.4						
10				1.5	1.5	70.4	20.6	3.0	1.5	1.5					
15				2.6		11.8	67.1	11.2	4.0	2.6		0.7			
25						4.0	8.0	18.9	55.6	8.8	2.0	2.7			
37.5						1.4	2.8	4.2	11.2	16.1	48.9	13.8	0.7	0.7	0.7
50							1.4	0.7	4.2	6.3	23.6	54.1	3.4	4.2	2.1

The enclosed indicating cutout is preferred by 70 per cent of the companies for fusing the primary circuits of distribution transformers. The enclosed nonindicating cutout is preferred by 22 per cent of the companies, whereas only 8 per cent expressed a preference for the open type. The majority of operators prefer the enclosed cutout at this voltage for safety considerations. Because of the relatively close conductor spacing and pole-top congestion it is almost impossible for linemen to avoid contact with live parts. For this reason it is common practice to insulate as much of the equipment and as many conductors as is practical. The marked preference for the indicating cutout is more difficult to explain, as in the majority of applications there is no fundamental need for the indicator. The most reliable indication of a blown fuse is the telephone of an affected customer. However, the indicating cutout has the advantage of removing the organic material from across the gap left by the blown fuse, thus eliminating trouble caused by leakage currents across the fuse tube.

The development of repeating fused cutouts naturally raises the question as to their use in conjunction with distribution transformers. Most companies use only single-shot fuses on their distribution transformers because of their lower cost and the fact that almost all internal transformer faults are of a permanent nature. Repeating fused cutouts are used with distribution transformers to a limited extent, however, by eight per cent of the companies.

Because of the wide variations in systems and in the locations of fused cutouts it is difficult to apply them so as to keep within their published interrupting ratings. It is interesting to note that exactly 50 per cent of the companies apply cutouts at locations where the calculated

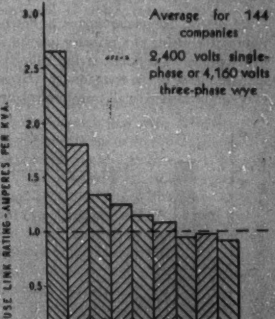

Figure 2. Weighted average of Table I—operating companies using various combinations of transformer and fuse ratings

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Paper 44-52, recommended by the AIEE committee on power transmission and distribution for presentation at the AIEE winter technical meeting, New York, N. Y., January 24–28, 1944. Manuscript submitted November 13, 1943; made available for printing December 14, 1943.

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Table II. Operating Companies Using Various Combinations of Transformer and Fuse Ratings

2,400-Volt Three-Phase Delta
100 Per Cent = 58 Companies

Single-Phase Transformer Rating—Kva (Multiply by 3 for Bank Kva)	Fuse Link Rating—Amperes														
	3	5	7	10	15	20	25	30	40	50	65	85	100	150	200
	Number of Companies in Per Cent														
5	1.6	21.0	11.3	61.3	4.8										
10		8.3		13.3	31.7	26.7	15.0	3.3	1.7						
15		1.5		14.7	11.8	19.1	32.4	13.2	7.3						
25				6.3	8.0		12.7	6.3	38.1	25.4	3.2				
37.5					7.1	3.6	5.4	1.8	7.1	35.7	14.3	25.0			
50							7.4	5.6	1.8	14.8	13.0	25.9	31.5		
75									8.5	8.5		10.5	59.6	12.8	
100										8.2	2.0	8.2	42.9	32.7	6.0

Table III. Operating Companies Using Various Combinations of Transformer and Fuse Ratings

4,800-Volt Single-Phase
100 Per Cent = 22 Companies

Single-Phase Transformer Rating—Kva	Fuse Link Rating—Amperes										
	1	2	3	5	7	10	15	20	25	30	40
	Number of Companies in Per Cent										
1.5	4.7	4.7	4.7	37.2	4.7	24.0					
3		9.1	9.1	50.0	4.5	22.8	4.5				
5			18.2	50.0	4.5	22.8	4.5				
7.5			4.5	45.5		41.0	4.5	4.5			
10			4.3	47.9		43.5	4.3				
15				9.1	13.6	68.3	4.5			4.5	
25						24.0	57.2	9.4	4.7		4.7
37.5						10.0	15.0	30.0	45.0		
50							5.0	20.0	50.0	10.0	15.0

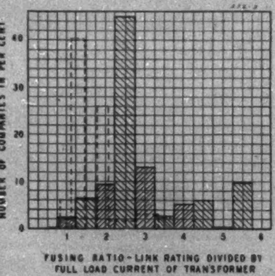

Figure 3. Weighted average fusing ratio for all ratings of transformers plotted from data of Table I

100 per cent = 144 companies

short-circuit currents are above their ratings.

Transformer Fuse Ratings

In general the major factor in determining the rating of a primary fuse for a distribution transformer is the kilovolt-ampere rating of the transformer. However, in the case of small transformers, up to five kilovolt-amperes or even larger, other factors usually control the fuse rating. The question was asked as to the minimum-size fuse link used on the system and the factors determining it. Lightning, transformer rating, and mechanical strength were the factors most frequently mentioned. The practice as to the minimum fuse rating employed is shown in Figure 1. The range of values is even wider than can be justified by variations in local conditions and indicates a lack of consistent practice.

Table I gives the percentage of operating companies using the various combinations of transformer and fuse ratings. The highest percentage value is in boldface type for each transformer rating. This table applies to 2,400-volt single-phase or 4,160-volt three-phase star systems. It is interesting to note that the old rule of "one ampere per kilovolt-ampere" is followed rather closely throughout the table except in the case of the smaller transformers. Here the rule has been dropped in favor of a higher fusing ratio because of the factors mentioned in connection with the selection of the minimum fuse rating. A weighted average of the table has been plotted in Figure 2. This figure brings out clearly the fact that the smaller transformers, particularly the 1.5 and 3 kva, are fused much higher than the larger ones.

The weighted average fusing ratio for all ratings of transformers has been plotted from the data of Table I and is shown in Figure 3. The ratio of the minimum melting point of the fuse link to the full-load current of the transformer will be twice that shown in the figure, as all these data are based on *N* rated links. Similar data gathered about ten years ago are plotted in dotted lines. Comparing the data of the present questionnaire with the previous data indicates a desirable trend toward higher fusing of

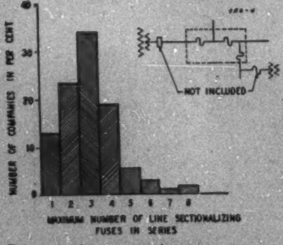

Figure 4. Line-sectionalizing practice employed by 123 companies

100 per cent = 123 companies

Table IV. Operating Companies Using Various Combinations of Transformer and Fuse Ratings

4,800-Volt Three-Phase Delta
100 Per Cent = 13 Companies

Single-Phase Transformer Rating—Kva (Multiply by 3 for Bank Kva)	Fuse Link Rating—Amperes													
	3	5	7	10	15	20	25	30	40	50	65	85	100	150
	Number of Companies in Per Cent													
5	7.2	50.0		42.8										
10		28.6		57.1		14.3								
15		7.2	21.4	21.4	28.6	21.4								
25				22.2	22.2		44.5		11.1					
37.5				8.3	8.3		41.8	8.3	25.0	8.3				
50					7.7	7.7	15.4	15.4	30.7	15.4				
75							11.1	11.1	11.1	44.5			11.1	11.1
100								6.7	13.8	20.0		33.3	20.0	6.7

A Short-Cut Method of Estimating Telephone-Influence Factor of Power Systems With Rectifier Load

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IN the planning of rectifier installations it is often desired to study the effect of the rectifier on co-ordination with communication systems. The telephone-influence factor[1] (*TIF*) is used in such studies as an index to the effect of distortion of the voltage and current-wave shapes on the inductive influence. Both voltage and current *TIF* are involved, because there are two types of induction:

1. Electric induction which depends upon the power-circuit voltage.
2. Magnetic induction which depends upon the current.

The current *TIF* is usually multiplied by the current in amperes, and the product is designated by $I \cdot T$. In some cases of existing exposures, neither the voltage *TIF* nor the $I \cdot T$ product is significant, and the inductive effects in such exposures are negligible. In other cases one or the other or both factors are significant in connection with existing exposures, as illustrated by Table I.

The study of rectifier circuits and power-system characteristics at harmonic frequencies has made it possible to obtain fairly good estimates of voltage and current *TIF* with rectifier operation, but the usual process is rather lengthy. This involves the calculation of system impedance for at least eight harmonic frequencies,[2] the harmonic analysis of the rectifier current wave[3,4] to obtain these eight or more harmonics, the weighting and summation of harmonic currents to get current *TIF*, the calculation of harmonic voltages from the values of current and of system impedance, and the weighting and summation of harmonic voltages to get the voltage *TIF*.

This paper describes a short-cut method of estimating the voltage and current *TIF* at the point where the rectifier is supplied, eliminating most of the steps just described by the use of curves based on average values of the factors involved. The method assumes that the supply system has an inductive reactance at harmonic frequencies, and the effect of resonance was neglected. The method does not give results that are close enough to justify its use in place of the detailed method where a thorough study is necessary, but, in most cases where it has been tried, it has given values that are near enough to test values to be representative, as will be shown later. In a complete study of an inductive co-ordination problem it may be desirable to estimate the voltage *TIF* and $I \cdot T$ product at points on the power system remote from the rectifier location, and the method described herein is not generally suitable for this purpose, except where the power system is relatively simple. In more complicated cases the use of the more detailed methods is suggested.

The method has been set up so that it can be applied to rectifiers with various numbers of phases, including 6, 12, 24, and up to 72 phases. The trend toward larger numbers of phases for the larger rectifier installations[3,4] may be seen from Figure 1 which represents some recent rectifier installations with a combined rating of over 2,000,000 kw. This group does not include all of the large

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distribution transformers. The old data which were given in terms of the 65 per cent rated fuse links has been converted to the present *N* rated links, and this explains the reduction in the width of the bars. One factor probably contributing to the increased fusing ratio is the change in method of rating fuse links. This automatically would increase the fusing ratio if the use of the same link ratings were continued.

Since the information contained in Table I applies to the majority of distribution systems, it has been given graphically in other forms in Figures 2 and 3 to bring out trends and factors which are not readily apparent from a table. Similar data are given in Tables II, III, and IV for other systems and are subject to the same treatment.

All companies replying use primary fused cutouts. Figure 3 shows the fusing ratios to be such, in most cases, that low-impedance secondary faults near the transformers will be cleared by the primary fuses. It is not surprising therefore, to find that only 12 per cent of the companies use fuses in secondary leads of some of their distribution transformers.

Line Sectionalizing

The questionnaire revealed that 85 per cent of the companies use cutouts for line sectionalizing. Of these, 54 per cent use them on the main feeders, 76 per cent on the three-phase branch feeders, and 83 per cent on single-phase laterals. The repeating cutout is most popular on the main feeders where 50 per cent are of this type, as contrasted with 41 per cent on the three-phase branches and 37.5 per cent on the single-phase laterals. About 70 per cent of the cutouts used for line sectionalizing, regardless of their location, are of the enclosed indicating type. Beyond the agreement that line sectionalizing is desirable, however, there is very little uniformity of practice. This is illustrated in Figure 4 where the maximum number of line-sectionalizing fuses employed in series, between the source interrupter and the distribution-transformer fuse, varies from one to eight. In those cases where the use of seven and eight fuses in series was indicated, it is probable that the source interrupter and the transformer fuse were included. If this is true, these cases should appear in the bars for five and six fuses.

A large amount of material can be found in the technical literature dealing with this subject, and much of it is of real value. It is recognized that this is a problem to which the most satisfactory solution can be found only by combining operating experience with theoretical analysis. The data included in this paper indicate that much of the present fusing practice is based principally upon operating experience without sufficient engineering interpretation. Further study of this problem should result ultimately in better system performance with fewer ratings and simpler devices.

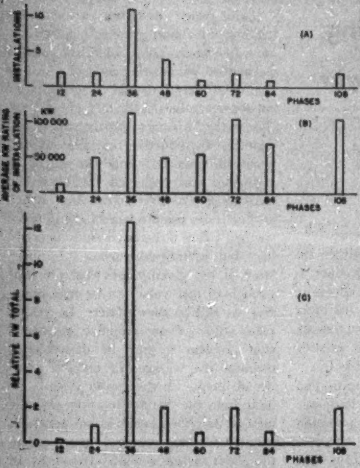

Figure 1 (left). Phase arrangements used in 26 rectifier installations, 10,000 kw and larger

A. Number of installations using each phase arrangement

B. Average kilowatt rating of installations for each phase arrangement

C. Relative total kilowatts for each phase arrangement

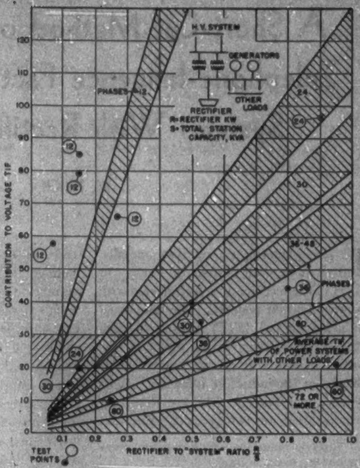

Figure 2. Curves for estimating voltage *TIF*, 60-cycle systems with rectifier load, 12 or more phases

Some of the test points were taken with the *TIF* meter in common use which is based on the 1919 weighting network (see reference 1). Figures in circles opposite test points show number of rectifier phases

installations but is believed to be representative of installations in service or under construction. Most of these installations are 36 phase, and the average installation rating for 36 phases is about 100,000 kw. It may be noted also that there are some installations with 72 or more phases.

This paper deals only with the *TIF* and does not consider other factors that are significant to inductive co-ordination. Some of the other factors are discussed in reports on the subject.[2,7]

The method described here is applicable to a-c systems and not applicable to d-c systems which are supplied by rectifiers. The treatment of the d-c case by methods already in use[5,7] is usually a fairly simple procedure.

Description of Short-Cut Method

This method of estimating voltage *TIF* makes use of a factor designated here as the rectifier to supply ratio *R/S*, because voltage *TIF* depends on the relative power capacities of rectifier and source. In the case of a rectifier supplied directly from a generating station, this is the ratio of the rectifier kilowatt rating to the combined generator kilovolt-ampere rating. When this ratio is used, the *TIF* is read directly from a curve sheet, such as that shown in Figure 2.

The curves were calculated by assuming a supply station of a certain kilovolt-ampere and voltage, having a reactance of ten per cent referred to the fundamental frequency, on the rated kilovolt-amperes and supplying a rectifier of average characteristics. A range of rectifier kilowatt ratings was assumed, extending from one tenth of the supply-station rating up to the full station rating (ratios of *R/S* from 0.1 to 1.0). This includes some conditions which were used for calculations but may not apply in practice to all of the phase arrangements covered; for instance, a generating station would not be loaded with 6- or 12-phase rectifiers up to its full capacity. The rectifier current was analyzed on a percentage basis by the methods available.[3,4] Harmonic currents in amperes were obtained by assuming equal values of rectifier and power-station ratings ($R/S=1.0$) for the initial calculation. These currents were multiplied by the station reactance, based on the ten per cent figure, to obtain the harmonic voltages on the station bus. These harmonic voltages were weighted and combined by the root-sum-square rule to obtain the *TIF* which was plotted at $R/S=1.0$ on Figure 2. This *TIF* value would come out the same for any assumed kilovolt-amperes and voltage, because the harmonic currents and the reactance were obtained on a percentage basis. For values of the ratio *R/S* less than 1.0 the rectifier operates at its own full load, and the percentage harmonic currents would be the same. Starting with the same station kilovolt-amperes, the reactance would be the same, but the currents in amperes would be proportional to the ratio *R/S*. Hence, the *TIF* would be reduced in proportion to *R/S* and could be represented by a straight line, as in Figure 2. This neglects the effect of station reactance on the current-wave shape which is unimportant in these approximate estimates.

Calculations were made for each num-

Table I. Significance of Various Influence Factors in Existing Exposures

	Type of Circuit		Significant Factor	
Case	Power	Communication	Exposure to Main Rectifier Feeder	Exposure to Branch Line
A	Cable	Either open wire or cable	Inductive effects generally negligible	
B	Overhead lines	Cable (toll)	Inductive effects generally negligible	
C	Overhead lines	Cable (local)	*I·T*†	*V-TIF** in some cases
D	Overhead lines	Open wire	*V-TIF* and *I·T*	*V-TIF*

* *V-TIF* Voltage *TIF*. † *I·T* Product of current times *TIF*.
This table is based on Table 6 of the Edison Electric Institute report.[7]

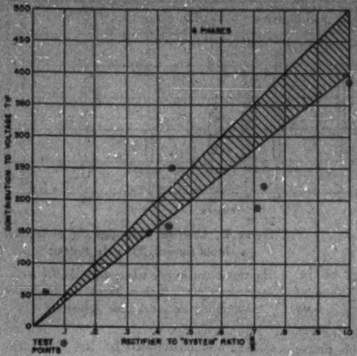

Figure 3. Curves for estimating voltage *TIF*, 60-cycle systems with rectifier load, six phases

The test points were taken with *TIF* meter in common use which is based on the 1919 weighting network (see reference 1)

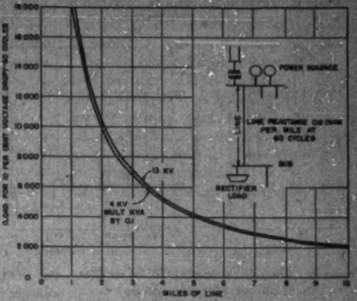

Figure 4 (right). Curves for estimating line kilovolt-amperes for *R/S* ratio of Figures 2 and 3

ber of phases, and a curve was plotted. It may be noted that the curves of Figure 2 are drawn as zones which cover a range from about ten per cent above to ten per cent below the calculated *TIF* values. This implies that estimates made from the curves are not expected to be closer than ten per cent, even if the simplifying assumptions which have been made are valid in the case under consideration. If the conditions differ materially from those assumed in preparing the curves, or if resonance between elements of the supply system is an important factor, the estimates may differ from the actual results by much more than ten per cent.

Voltage TIF of 60-Cycle Systems

The curves of Figures 2 and 3 apply to 60-cycle systems and cover all of the common phase arrangements. The *TIF* values are based on the 1935 weighting curve.[1] Estimates of *TIF* are made in the following manner: When the rectifier is close to the supply station, the rectifier-to-supply ratio *R/S* is obtained by dividing the rectifier kilowatt rating by the total kilovolt-amperes of the station. The estimated voltage *TIF* at the station is read from the point on the curve sheet corresponding to *R/S* and the number of phases. This is the contribution of the rectifier of the total *TIF*. Sometimes it may be desirable to state that the estimated *TIF* is between the two values indicated by the zone. The procedure just described applies to rectifier load close to a generating station, a substation tied in with a larger power system, or a station which combines these two conditions, in which case the combined kilovolt-ampere rating of generators and transformers which feed into the same bus is used. The procedure for rectifier load at some distance from the source is described in a paragraph which follows. The curves are based on ten per cent reactance referred to 60 cycles. Allowance for other per cent reactance values can be made, as illustrated in one of the examples given later (see example 1).

The total *TIF* of the station-bus voltage may be estimated by taking the square root of the sum of the squares of the rectifier contribution and the system *TIF* without the rectifier. If the system *TIF* is not known, an average value may be estimated from Figure 2.

When the rectifier is at some distance from the supply station, the line reactance may be the determining factor for the voltage *TIF* at the rectifier. The curves of Figures 2 and 3 may be applied to this case by using line kilovolt-ampere instead of station kilovolt-amperes. This is the kilovolt-ampere load which would produce a reactive voltage in the line equal to ten per cent of the line voltage at 60 cycles. Figure 4 gives curves for lines up to ten miles long at 4 kv and 13 kv, from which values of line kilovolt-ampere can be obtained. For other voltages the kilovolt-ampere value may be obtained by reading the kilovolt-amperes from one of the curves and multiplying by the square of the ratio of actual kilovolts to kilovolts on curve. Also, for other lengths of line the kilovolt-amperes may be read for an assumed length and multiplied by the ratio of the assumed length to the actual length. After the *R/S* ratio has been obtained, the curve is used

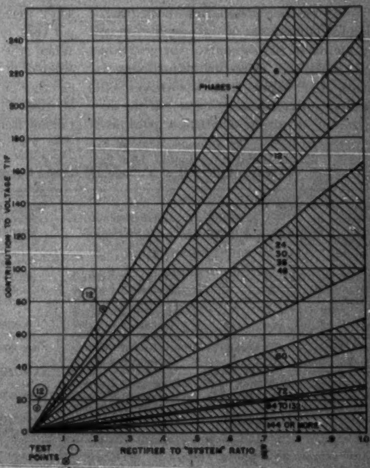

Figure 5. Curves for estimating voltage *TIF*, 25-cycle systems with rectifier load

The test points were taken with the *TIF* meter in common use which is based on the 1919 weighting network (see reference 1). Figures in circles opposite test points show number of rectifier phases

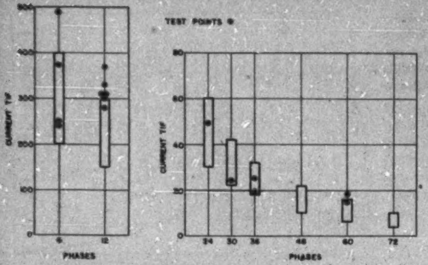

Figure 6. Chart for estimating *TIF* of rectifier input current, 60-cycle systems

Some of the test points were taken with the *TIF* meter in common use which is based on the 1919 weighting network

as already described. Sometimes both the line and the station reactances are determining factors. In such cases a value of kilovolt-amperes is determined which gives ten per cent reactive voltage in the line and station combined. This can be shown best by means of an example given later (see example 4).

The curves were calculated on the basis of inductive reactance. In power systems with cable circuits or extensive overhead lines, capacitance is a factor. In some cases resonance occurs at certain harmonic frequencies which may increase or decrease the voltage *TIF*. The method described here does not allow for resonance. The more detailed methods provide for taking resonance into account when necessary, but some approximations are involved.

Some rectifier installations have provision for adjustment of the output voltage by means of phase control (or grid control). If the range of adjustment is small, *TIF* values from the curves will be close enough in most cases. If the adjustment by phase control is over five per cent, the voltage *TIF* may be increased in the order of 2 to 1. One of the references[3] gives some limited test data on this point, and another[8] gives some calculations on effects of phase control.

The available measured values of *TIF* have been plotted on Figures 2 and 3 for comparison with the curves. Most of these values have been published.[2,7] A majority of the 19 test points are within or reasonably close to the proper zones. The points farthest from the curve are three points for the 12-phase rectifiers where the measured values are 1.6 to 2 times the values from the curves. These tests were made on overhead systems, where resonance conditions existed, and increases of this order would be expected.

Voltage TIF of 25-Cycle Systems

The curves of Figure 5 apply to 25-cycle systems and correspond to those of Figures 2 and 3 for 60-cycle systems. They were calculated on the basis of ten per cent reactance referred to 25 cycles. When the voltage *TIF* is to be calculated at a rectifier which is at some distance from the supply station, values of line kilovolt-amperes may be obtained from Figure 4, which is for 60 cycles, and multiplied by 2.4, the ratio of 60 to 25. This conversion is necessary, because reactance voltage is proportional to frequency. The available *TIF* measurements on 25-cycle systems have been plotted on Figure 5, and their agreement with the curves is comparable with the 60-cycle data.

Table II. Estimates of Voltage *TIF* for the Conditions of Example 3

Rectifier (Kilowatts)	Number of 24-Phase Units	Phase Arrangement	Combination Assumed for Calculation (Values of R)	Generating Station Kilovolt-Amperes (S)	R/S	TIF Component for Each Group	TIF for Combination
40,000	2	24	24-phase $R=40{,}000$	80,000	0.5		60
40,000	2	48	48-phase $R=40{,}000$	80,000	0.5		35*
60,000	3	48 and 24	48-phase $R=60{,}000$	80,000	0.75	53	60
			24-phase $R=20{,}000$		0.25	28	
60,000	3	72	72-phase $R=60{,}000$	80,000	0.75		13*
80,000	4	48	48-phase $R=80{,}000$	160,000	0.5		35
80,000	4	72 and 24	72-phase $R=80{,}000$	160,000	0.5	13	20*
			24-phase $R=20{,}000$		0.125	15	
100,000	5	72 and 24	72-phase $R=100{,}000$	160,000	0.63	10	30*
			24-phase $R=40{,}000$		0.25	28	
100,000	5	48 and 24	48-phase $R=100{,}000$	160,000	0.63	45	55
			24-phase $R=20{,}000$		0.125	33	
120,000	6	48	48-phase $R=120{,}000$	160,000	0.75		55
120,000	6	72	72-phase $R=120{,}000$	160,000	0.75		13*

* Arrangements for minimum *TIF*.

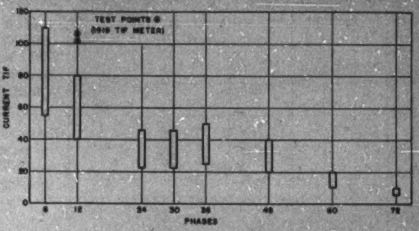

Figure 7. Chart for estimating *TIF* of rectifier input current, 25-cycle systems

The test points were taken with the *TIF* meter in common use which is based on the 1919 weighting network (see reference 1)

Current TIF of 60-Cycle Systems

The charts in Figure 6 give approximate values of current *TIF*, based on the 1935 weighting curve, for rectifiers on 60-cycle systems. The same phase arrangements are covered, as in Figures 2 and 3, for voltage *TIF*. The shaded areas allow for the spread between different circuit conditions. The estimated *I·T* product is obtained by multiplying the rectifier current in amperes, in the a-c line supplying the rectifier, by the *TIF* from the chart. The available test data from the sources referred to under voltage *TIF* have been plotted on the charts for comparison with the calculated values. Reasonable agreement is shown in most cases. Phase control may increase the current *TIF* when the voltage adjustment is more than five per cent.[3,8]

Current TIF of 25-Cycle Systems

The charts in Figure 7 give approximate values of current *TIF* for rectifiers on 25-cycle systems. They correspond

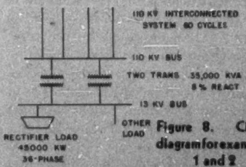
110 KV INTERCONNECTED SYSTEM 60 CYCLES
110 KV BUS
TWO TRANS 35,000 KVA EACH 8 % REACT
13 KV BUS
RECTIFIER LOAD 45000 KW 36-PHASE
OTHER LOAD

Figure 8. Circuit diagram for examples 1 and 2

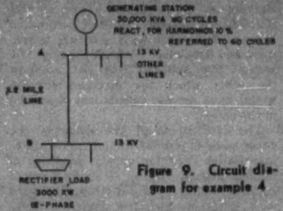

Figure 9. Circuit diagram for example 4

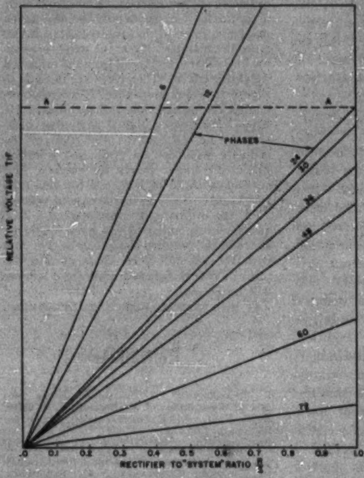

Figure 10 (left). Tentative curves for estimating voltage *TIF* with proposed weighting curve, 60-cycle systems with rectifier load. For ordinates see Figure 12

to those of Figure 6 for 60-cycle systems. The available test data for 25-cycle systems are indicated by test points.

Illustrative Examples

The examples which follow are given for the purpose of showing how the data in this paper might be applied to specific rectifier situations.

Example 1

It is desired to estimate the voltage *TIF* on the 13-kv bus of the station shown in Figure 8 which supplies 45,000-kw rectifier load, 36 phase. The system *TIF* without rectifier load is 15.

The station capacity is 70,000 kva with eight per cent reactance. Since the curves are based on ten per cent reactance, we use 70,000×10/8=87,500 kva. Then $R/S=45{,}000/87{,}500=0.52$. With reference to Figure 2, the *TIF* for 36 phases corresponding to $R/S=0.52$ is between 32 and 40, or about 36. The over-all *TIF* at the bus is $\sqrt{36^2+15^2}=39$.

In a test on a 36-phase rectifier, where the rectifier kilowatts and the station-transformer kilovolt-amperes were in the same ratio as in this example, the measured *TIF* was 34 as compared with the calculated value of 39.

Example 2

The voltage *TIF* on the 13-kv bus of example 1 is to be estimated with one sixth of the rectifier plant shut down. This is one of the six six-phase groups of which the 36-phase installation is composed. The 36-phase connection suppresses all of the six-phase harmonics up to the 35th and 37th. The vector sum of five units is therefore equal in magnitude to one unit for these harmonics. The 35th and 37th harmonics, however, are all in phase. If it is assumed that the full load is being carried by the five units, the combination will be treated as a 36-phase rectifier of the total rating and a superimposed 6-phase rectifier of one fifth of the total capacity.

For the 36-phase component

$$\frac{R}{S}=\frac{45{,}000}{87{,}500}=0.52$$

$$TIF=36 \quad \text{from Figure 2}$$

For the six-phase component

$$\frac{R}{S}=\frac{1}{5}\times\frac{45{,}000}{87{,}500}=0.1$$

$$TIF=40$$

Total rectifier contribution to *TIF*

$$=\sqrt{36^2\times 40^2}=54$$

Over-all *TIF* at bus $=\sqrt{54^2+15^2}=56$

If the six-phase group is removed, the *TIF* thus increases from 39 (example 1) to 56.

In the test referred to in example 1 the measured *TIF*, with one six-phase unit shut down and the other five carrying the station load, was 61. This is slightly higher than the calculated value of 56, possibly because phase control was being used to some small extent to equalize the division of load between the rectifier units.

Example 3

In a preliminary study of a proposed rectifier installation, it is desired to estimate the effect of several combinations of rectifier units and phase arrangements.

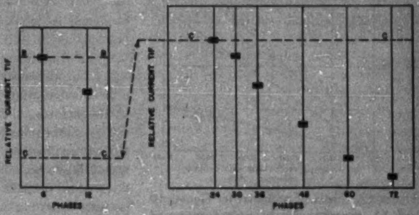

Figure 11 (right). Tentative chart for estimating current *TIF* with proposed weighting curve, 60-cycle systems with rectifier load. For ordinates see Figure 12

Each unit is 20,000 kw, 24 phase. The initial installation is to be 40,000 kw (two units) supplied from a station of 80,000-kva capacity. A third unit is to be added later. Still later the generating station is to be increased to 160,000 kva and three more units added, one at a time. The frequency is 60 cycles. Since the generator reactance is not known, it is assumed to be ten per cent for harmonics.

Several combinations of 24, 48, and 72 phases might be used. Combinations, such as two units combined to give 48 phases and one additional 24-phase unit, are treated as follows. Since the 47th and 49th harmonic components are additive, a 48-phase rectifier of the full kilowatt rating (the combined rating of three units) is assumed and its *TIF* contribution taken from Figure 2. The additional contribution of the 24-phase unit is controlled mainly by the 23rd and 25th harmonics, and its value is taken from Figure 2 for the rating of the 24-phase unit. The two components are combined as the square root of the sum of the squares. The calculations and the results are shown in Table II.

The preferable combinations from the standpoint of obtaining as low *TIF* values as possible are indicated by the asterisk. These combinations are 48 phase for two units (initial installation), 72 phase when the third unit is added, 72 phase and 24 phase with four and five units, and finally 72 phase for the whole installation of six units. The conversion from 48 to 72 phase when the third unit is installed could be provided for in the beginning by furnishing phase-shifting transformers with suitable taps to reduce the phase displacement.

Example 4

With reference to Figure 9, it is desired to estimate the voltage *TIF* at the station bus *A*, the rectifier bus *B*, and the *I·T* product for the rectifier feeder *AB*. The rectifier rating is 3,000 kw, 12 phases, and the generating-station rating is 30,000 kva. The rectifier feeder is 1.2 miles long.

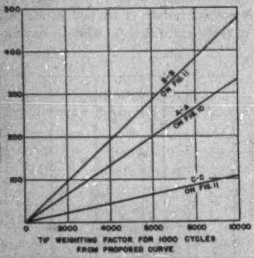

Figure 12. Scale of ordinates for Figures 10 and 11 corresponding to various proposed scales for new *TIF* weighting curve

To estimate the *TIF* at the station bus *A*, the ratio $R/S = 3{,}000/30{,}000 = 0.1$ is used. If we refer to Figure 2, the *TIF* is about 30.

To estimate the *TIF* at point *B*, the following calculation is made:

line kilovolt-amperes = 17,500 for 1.2 mile line (from Figure 4)
station kilovolt-ampere = 30,000
total kilovolt-amperes for *S* at ten per cent is then

$$\frac{17{,}500 \times 30{,}000}{47{,}500} = 11{,}000$$

$$\frac{R}{S} = \frac{3{,}000}{11{,}000} = 0.27$$

With reference to the curve for 12-phase rectifiers in Figure 2 the *TIF* is approximately 80.

To estimate the *I·T* product for the feeder, the current *TIF* is about 225, from Figure 6. The line current is

$$I = \frac{3{,}000}{1.73 \times 13} = 133 \text{ amperes}$$

Then

$$I \cdot T = 133 \times 225 = 30{,}000 \text{ approximately}$$

In a test where the ratio of rectifier kilowatts to station kilovolt-amperes and the ratio of line reactance to generating-station reactance were about the same as in this example, the measured *TIF* at the rectifier was 65. Also, the measured *I·T* referred to 13 kv, since the line voltage was different, was 42,000.

Conclusion

Herein is described a practical short-cut method of estimating power-system *TIF* at a rectifier supply point. It is based on previously demonstrated methods of calculating harmonics produced by rectifiers together with certain simplifying assumptions as to the characteristics of rectifier and power circuits. The results are reasonably consistent with test data. The method should be useful, especially where a number of conditions have to be investigated in a preliminary study, since it requires only a few minutes to make the calculations for each condition when the pertinent data are at hand. The results may be useful also for checking other calculations. The method is applicable to the arrangements of 24 or more phases that are being used for large rectifier installations as well as to the 6- and 12-phase arrangements used for the smaller stations. In a co-ordination study the *TIF* has to be considered in combination with other factors, such as the type and location of circuits. This is pointed out in the introduction.

Appendix

Recent tests on telephone equipment have resulted in a new frequency weighting curve for message circuit noise.[2] A proposed *TIF* weighting curve based upon these data has been agreed upon as to shape. The magnitude of the peak or other ordinate has not been decided upon yet. The next step in the program is to make studies of electric apparatus and systems with the object of arriving at the weighting curve of this shape which will give the best correlation between data taken with new type instrument and data taken with the existing *TIF* meter. This appendix gives data to show the effect which the proposed revision would have on the calculations described in the paper.

The establishing of the shape of the curve made it possible to plot curves showing relative *TIF* for various numbers of rectifier phases. Figure 10 shows a set of curves of voltage *TIF* for 60-cycle systems with rectifier load which is similar to Figure 2 for the 1935 weighting curve, but the ordinates are not marked. However, a scale of ordinates can be drawn for any given weighting curve of the required shape in the following manner: First, the weighting factor for 1,000 cycles is read from the proposed weighting curve. Then reference is made to Figure 12 which gives the *TIF* corresponding to the dotted line *AA* on Figure 10. The *TIF* corresponding to any other ordinate can be obtained by proportion, or the ordinate scale can be graduated suitably. Figure 11 shows charts for current *TIF* similar to those of Figure 6 for the 1935 weighting curve. As in the case of voltage *TIF*, the ordinates are not marked. *TIF* values corresponding to any given weighting curve of the required shape can be obtained with the aid of Figure 12 as already described. It is believed that these curves will be helpful in studies of the proposed *TIF* with reference to rectifier installations.

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The Functions of Ground Preference in Carrier-Current Relay Schemes

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ALMOST all of the more common present-day carrier relay systems employ a feature known as ground preference which enables the ground relays, during faults involving ground current, to take preference over the phase relays in controlling the starting and stopping of the carrier signal. The specific system conditions which require this feature are not well understood, nor is it generally understood that under certain conditions ground preference can cause trouble, or that even in some cases phase preference is "preferable." It is the purpose of this paper to discuss, in general, the reasons for and conditions pertaining to the preference feature.

The most common type of carrier relay system now in general use is the intermittent carrier system employing distance-type relays to control carrier, carrier being transmitted only in the unfaulted line sections to block tripping. There are two variations of this scheme; one using a three-zone impedance relay to start and stop carrier by circuit-closing contacts, and another using a three-zone reactance relay to start carrier by a circuit-opening contact and to stop carrier by a circuit-closing contact. These two arrangements are shown in Figure 1 in only enough detail to illustrate how the preference feature is obtained. The circuit-closing arrangement is shown in Figure 1a. Closure of the phase or ground start contacts connects the cathode of the master oscillator tube to battery negative, which is the condition for oscillation; and opening of the stop contacts returns the grid to the nonoscillating condition. The phase start contact is operated by a fault-detector impedance element and the phase stop contact is operated by an element whose coil is energized by a second impedance element whose contacts are in series with the single-phase directional-element contacts. The ground start contact is operated by an overcurrent element with a low current setting while the ground stop contact is operated by an auxiliary element energized from an overcurrent element with a high setting, whose contact is in series with the ground directional contact. It is evident from this diagram that the ground relays can start and stop carrier regardless of the position of the phase contacts. Also it is clear that a phase preference could be obtained by interchanging the position of the phase and ground start and stop contacts and that no preference is obtained by paralleling the phase and ground start contacts. Figure 1b shows the schematic connections for the reactance-relay carrier system. Carrier is started by opening any one of the voltage-restrained fault-detector contacts, thus removing the carrier control lead from battery negative which is the carrier-transmission condition. Carrier can then be stopped by the closure of the phase stop contact which is operated by a single-phase voltage-restrained directional element. Carrier is started on ground faults by the opening of the ground start contact on a low-set overcurrent relay and is stopped by the closure of a high-set overcurrent contact and a directional contact. It is clear that the ground relays have preference in controlling over the phase relays. Also, phase preference or no preference can be obtained by the proper regrouping of the contacts.

There are a number of system conditions presented to the relays during faults involving ground current which may cause failure to block through carrier unless steps are taken to prevent such action. The ground-preference feature is one of these steps. Fundamentally, the trouble results from the fact that during single-phase-to-ground faults, and especially during two-phase-to-ground faults, both the phase and ground relays respond. It is possible that, under certain conditions, the ground relays will see an external fault as such, whereas the phase relays may view the situation as an internal fault. On the other hand, under certain other conditions, the phase relays may view the situation correctly, but the ground relays may not see the correct picture.

An example where the phase relays may see a fault as internal, whereas it is an external fault, correctly seen by the ground relays, is the case where the current fed through a line section to an external fault is mostly zero sequence. Figure 2 depicts a condition of this kind which has been made extreme for simplification. For a single-phase-to-ground fault on the line out of substation *D*, power is fed from station *B* to the grounding bank at *A*, and zero-sequence fault current is fed over all three phase wires in the line section *C—D* to the fault as is shown by the solid arrows. A power source at *E* would feed current over only one phase wire in the line from *C* to *D*.

While the conditions shown are not typical, in that a grounding bank has its power source at another point in the system, the conditions on any grounded system will approach the aforementioned conditions. That is, any transmission line between a source of power and a grounding point will carry in-phase fault currents on all three phase wires. The application of carrier relays to the line section *C–D* requires consideration of this fact. An examination of the action of single-phase directional elements reveals that under the aforementioned conditions the carrier signal at *D* is liable to be removed incorrectly by one of the phase relays. Since carrier is removed correctly at *C* by the ground relay, failure to block on the external fault results. It is evident that if star currents are used on the phase directional relays the three single-phase directional elements at *D* cannot all indicate the same direction since the currents in all three are inphase but the voltages are considerably out of phase.

As will be easily seen from Figure 3,

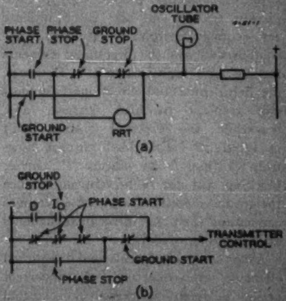

Figure 1. Schematic connections of ground-preference arrangement

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which depicts the voltages and currents applied to the relays at location D, directional elements with a watt characteristic and a 30 degree connection, for example, will indicate both directions. That is, the A-phase element using I_A current and E_{CA} voltage will open contacts for a close in external fault; also the B-phase element will open contacts and the C-phase element will close contacts.

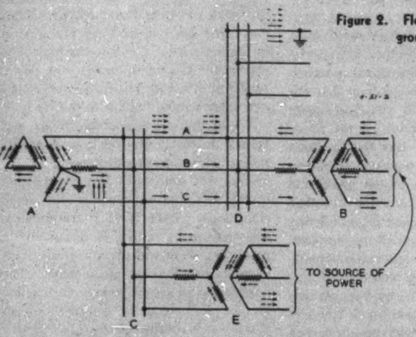


Figure 2. Flow of fault current on a grounded system

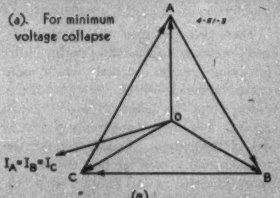
In view of the foregoing it can be stated that any type of single-phase directional element used for the purpose of controlling carrier will cause failure to block tripping for external ground faults under one or another of the conditions met with on a grounded power system.

The preceding is discussed on the basis that star current is used on the directional element. However, a carrier system using single-phase watt-type directional elements energized from delta current gives a different answer. It is evident that the delta connection of currents (Figure 4a), wherein the A-phase element receives I_A-I_B, the B-phase element receives I_B-I_C and the C-phase elements receive I_C-I_A, completely cancels out the three inphase zero-sequence currents represented by the solid arrows, and the directional elements receive no fault current. However, the delta-connected directional elements will respond to load current, and, if, while the external ground fault in Figure 2 exists, load current flows from station D to station C, carrier blocking will not be accomplished since the ground directional relay at C removes carrier and the phase directional elements at D also remove carrier.

As far as the aforementioned directional elements are concerned, the incorrect action in removing carrier on external ground faults can be eliminated by the use of a ground-preference connection such as shown in Figure 1. There it is obvious that the incorrect indication of the phase directional elements on ground faults is of no concern since the ground relays can start and stop carrier regardless of the phase relays. The facts brought out in the foregoing yield some pertinent observations. If star currents are used in the phase directional elements the ground-preference feature definitely is needed, since the elements give false directional indications because of the flow of zero-sequence fault currents. Furthermore, the incorrect indication of the directional relay on zero-sequence fault current is augmented by the flow of load current in the opposite direction. In other words, load current flowing from station D to C on phase C adds more or less inphase to the zero-sequence fault current from station C.

Phase directional relays using delta currents require the ground-preference feature only because of the possible flow of load current opposite to the zero-sequence fault current.

(a). For minimum voltage collapse



(b). For maximum voltage collapse

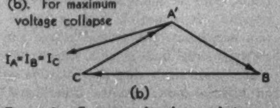


Figure 3. Current and voltage relations at station D

So far, consideration has been given only to the zero-sequence fault current from the grounding bank A as represented on Figure 2 by the solid arrows. On any actual power system, however, there may be a flow of positive-, negative-, and zero-sequence current from another source of power such as the transformer bank E. The line currents flowing from bank E to the fault are shown by the dotted arrows. Note that the bank E fault-current contribution in section C–D flows only in the faulted phase A. It is interesting to see what effect this current has in connection with the use of the ground-preference feature. When star current is being utilized, it is quite evident that the directional response of the three directional elements is not at all changed, since the additional phase-A current merely supplies more opening torque to the phase-A element and does not affect the incorrectly responding phase-C element.

On the other hand when the directional elements are supplied with delta current, the fault current on phase A from bank E causes the A-phase and C-phase directional elements to open contacts. This is seen from Figure 4b where I_A (representing current flow toward station D) with E_{CA}' and $I_C = -I_A$ with E_{BC} give opening torque. The flow of 100 per cent-power-factor load current from station D to C is represented by the star currents inside the triangle CA'B, and it is seen that the load current on phase C is substantially opposite to the fault current I_C. This means that the phase-C directional element cannot operate in the wrong direction unless the load current

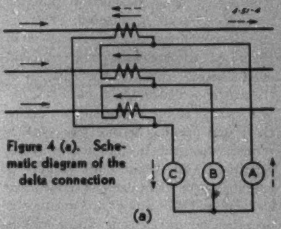


Figure 4 (a). Schematic diagram of the delta connection

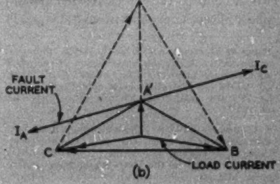


Figure 4 (b). Vector relations

predominates over the phase-*A* fault current which is supplied by the bank *E*. However, fault current does not appear in the phase-*B* relay, and, therefore, its directional element may close contacts because of load current.

Heretofore, only the directional element has been discussed. However, present day distance-relay carrier systems employ a distance-measuring element to supervise the directional element. In one system a separate impedance element *Z2* has its contacts in series with the directional element to stop carrier and complete the trip circuit through the receiver relay contacts. Another system uses voltage restraint on the directional element to accomplish the same purpose effectively.

It is now necessary to see how the use of the supervising distance element affects the requirement for ground preference. If the case where the directional elements receive delta current are considered first, the impedance elements would also receive this same current, and the effect of the impedance element is as follows: If all of the fault current from *C* to *D* is zero-sequence current, the relay at *D* is subject only to load current, and it can stop incorrectly carrier transmission for load current flowing from *D* to *C* only if this load current is high enough to reach the pickup point of the impedance element at the voltage existent during the fault. For an external ground fault on phase *A*, for instance, the voltage on *A*-phase and *C*-phase impedance elements may be anything between the limits of normal for a distant fault to 58 per cent of normal for a close-in fault. In order to close the phase-*B* relay contacts the load current would have to operate against full normal voltage. Ordinarily, the setting of the supervisory impedance element would be considerably above the maximum load current.

When the additional fault current flowing on phase *A* only is considered, the conditions for incorrect operation of the relays at station *D* became even more difficult to attain. As was seen previously, the fault current on phase *A* causes the *C*-phase relay to act correctly and counteracts the effect of the load current. The only condition where phase-*C* relay could indicate the wrong direction would be for the load current from *D* to *C* to be larger than the fault current on phase *A*. A slightly predominant load current could reverse the phase-*C* directional contacts, but in order to operate the supervising impedance element the predominance would have to be considerably greater than this to reach the pickup of the impedance element whose voltage is greater than 58 per cent of normal. The action of the phase-*B* relay must also be considered even though, as is seen from Figure 4a, it receives no fault current. However, there is load current in phase -*B* relay, but to get incorrect action out of this relay, the load from *D* to *C* must reach the pickup of the impedance element which has full normal voltage, and, of course, this is impossible.

In the case where star current is used, the addition of the distance-measuring element to supervise the directional element does not lessen materially the chances for incorrect operation. As was shown previously, the zero-sequence fault current on phase *C* may be of sufficient magnitude to operate the distance element since the restraint voltage may be reduced to 58 per cent of normal. Furthermore, this element may also be subject to zero-sequence fault current plus load current, and under these conditions it is quite possible that the phase-*C* relay will try to remove carrier.

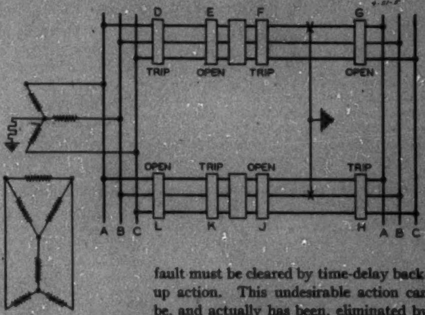


Figure 5. Schematic diagram of system requiring phase preference

The fault current from bank *E*, Figure 2, which flows only on phase *A* would, of course, have no effect on the phase-*C* relay.

The preceding considerations lead to these conclusions:

1. If the phase directional and distance supervising elements use star current, then ground preference definitely is needed to prevent incorrect removal of carrier on single-phase-to-ground faults because of the flow of zero-sequence fault current on the unfaulted phases and also because this current adds to the load current flowing in the opposite direction.

2. If delta current is used, then ground preference is required only if the through-fault current is all zero-sequence current and the load current in the opposite direction is large enough to reach the pickup value of the supervising impedance element at 58 per cent of normal voltage. Such a set of conditions is comparatively rare, and, therefore, if delta current is used, ground preference can be eliminated in order to take care of other conditions if necessary.

While, heretofore, the ground-preference arrangement has been used in most carrier installations with good results, it nevertheless has, upon occasions, been desirable to have some other form of preference. One instance where the ground-preference connection causes trouble is the case of a two-phase-to-ground fault involving a high ground resistance. Here the ground current may be between the pickup values of the ground-tripping overcurrent element and the ground carrier start overcurrent element, in which case carrier is put on by the ground relays but not taken off, thus blocking the phase relays from high-speed tripping, and the fault must be cleared by time-delay backup action. This undesirable action can be, and actually has been, eliminated by removing the overcurrent element from the carrier stop circuit and making the removal of carrier by the ground relays dependent only upon the ground directional relay. This, of course, requires that the sensitivity of the ground directional relay be the same as that of the overcurrent carrier start relay. However, the sensitivity of the carrier stop element cannot be made too great, otherwise failure to block will result on external phase faults where system unbalances may produce enough spurious ground current to cause the sensitive ground-carrier stop element to operate.

While the ground-preference connections have been used extensively in carrier relay system, heretofore, it should be realized that there are certain system conditions which require phase-relay preference rather than ground preference. A specific instance where phase preference is required is the case of simultaneous ground faults in different line sections.

Influence of Improved Magnetic Alloys on Design Trends of Electrical Instruments

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Synopsis: Small panel-type electrical indicating instruments are recognized as an important component in radio, electronic, and power control equipment, which is so essential in all military and naval operations on land, in the air, and on the sea. Such instruments are required to be small, compact, light in weight, and reliable measuring devices; and one of the major factors in the realization of these requirements is the efficient utilization of improved magnetic materials for permanent magnets.

This paper describes properties of a new magnetic alloy and compares its characteristics with other permanent-magnet materials used in small panel electrical indicating instruments of the permanent-magnet moving-coil type. The use of a new cobalt-molybdenum-iron alloy as a permanent magnet material in an indicating instrument and its influence on design trends are discussed. The resulting improvement in overall instrument performance by the use of this new material is summarized, with particular reference given to mechanical factor of merit which is a measure of instrument reliability.

THE use of new and improved permanent-magnet alloys which have been developed during recent years probably has influenced the design trend of electrical indicating instruments more than any other one factor. For given sensitivities, instruments have been made available which are more sturdy and reliable, and instruments of higher sensitivity have been made possible by the use of the newer alloys. Relatively higher coercive force *Hc* and high residual *Br* materials are now available that are readily machined and use a minimum of critical materials, factors which are of particular importance in high production designs for wartime applications.

Characteristics of Permanent-Magnet Alloys

The properties of 11 commercially available permanent-magnet alloys recently published[1] are shown in Table I.

Until comparatively recently, the chrome and tungsten alloy steels were the principal materials used for permanent magnets in electrical indicating instruments, replacing the unalloyed carbon steels previously used. These alloys possess fair machining qualities and can be fabricated by either hot-forging or punching into various sizes and shapes, but they are inherently low in available energy and require a relatively large ratio of length of magnet to length of air gap. Magnets from these materials are frequently manufactured from preshaped rods or bars, cut to length, heated, and formed to the required shapes while hot by the use of special equipment. After cooling, subsequent grinding, drilling, and machining operations are performed, after which they are hardened and the air gap accurately ground. A typical finished magnet is shown in Figure 1A.

Soft-iron pole pieces are then used to provide the correct flux distribution and to insure permanency of the flux pattern. Figure 1B shows a typical magnet and pole-piece assembly.

Low coercive forces *Hc* in the order of 60 to 70, residual flux density *Br* of 9,000 to 10,300 gausses, and total relative energy of 295,000 to 320,000 (*BH* maximum energy) are typical values for the magnetic characteristics of these alloys.

Some magnets which are used for small panel-type instruments are made from these materials in the form of laminations of thicknesses of approximately one sixteenth of an inch punched from sheet material to final shape in conventional dies and stacked to desired height after hard-

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On systems grounded through an impedance it sometimes happens that a flashover to ground on one phase may immediately cause another flashover on another phase in another line section. Analysis of the action of directional ground relays under these conditions shows that the directional indication at several locations may be incorrect. The situation is depicted schematically in Figure 5, and it is seen that, for a ground fault on phase *A* in section *FG* and on phase *B* in section *JH*, one ground relay in each section except *DE* as indicated by "open" and "trip" operates incorrectly. If carrier relaying with ground preference were applied to this system, incorrect operation would result in both faulted sections. In section *FG* the phase relays at *F* would remove carrier but would be blocked by carrier from *G* which is held on by the ground relay. (Blocking of carrier transmission by the fault is an exception). In section *JH* the same action would occur. If the carrier connections were changed to give preference to neither the ground relays nor the phase relays, then correct tripping would occur in the faulted sections because the phase relays would clear the faults. However, carrier blocking would not be obtained in section *LK*, since the phase relays would remove carrier at *L* and the ground relays would remove it at *K*. If preference were given to the phase relays, correct action in all sections would result, since the phase-relay directional indications are correct, and the distance elements easily can be set to respond to this type of fault.

Conclusions

1. The necessity for the ground-preference feature in carrier relay systems depends, to a large extent, on the type of current connections used in the phase relays.

2. When star currents are used in the phase relays, the ground preference feature definitely is needed during the flow of fault current and load current.

3. If delta currents are used in the phase relays, the ground-preference feature is needed only in cases where the fault current is all zero sequence and extreme load currents are present.

4. Phase preference is required on systems subject to simultaneous grounds in different line sections, and if delta currents are used the phase-preference connection is feasible.

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Table I. Properties of Commercial Permanent-Magnet Alloys

Alloy	Typical Composition (Per Cent)	Coercive Force (Hc)	Residual Induction (Br)	B×H (Maximum)	Mechanical Properties	Commercial Methods of Fabrication
1. Chromium magnet steel	3.5 Cr; 1 C; remainder, Fe	63	9,000	295,000	Hard and strong	Hot forge, punch and machine
2. Tungsten magnet steel	5 W; 1 C; remainder, Fe	70	10,300	320,000	Hard and strong	Hot forge, punch cast, machine
3. Cobalt (36 per cent) magnet steel	36 Co; 3.5 Cr; 1 C; remainder, Fe	210	9,000	930,000	Hard and strong	Hot forge, punch cast, machine
4. Cobalt-nickel-copper alloy	41 Co; 24 Ni; 35 Cu	440	5,300	993,000	Ductile	Cold-roll, machine, punch cast
5. Iron-nickel-copper alloy	20 Fe; 20 Ni; 60 Cu	450	5,800	1,070,000	Ductile	Cold-roll, machine punch
6. Comol magnet alloy	12 Co; 17 Mo; remainder, Fe	245	10,300	1,100,000	Hard and strong	Hot forge, punch machine, cast
7. Alnico IV	12 Al; 28 Ni; 5 Co; remainder, Fe	700	5,200	1,250,000	Hard and brittle	Cast, sinter, or grind
8. Alnico III	12 Al; 25 Ni; remainder, Fe	400	7,100	1,330,000	Hard and brittle	Cast, sinter, or grind
9. Alnico I	12 Al; 20 Ni; 5 Co; remainder, Fe	400	7,100	1,330,000	Hard and brittle	Cast, sinter, or grind
10. Alnico II	10 Al; 17 Ni; 12.5 Co; 6 Cu; remainder, Fe	540	7,200	1,650,000	Hard and brittle	Cast, sinter, or grind
11. Alnico V	8 Al; 14 Ni; 24 Co; 3 Cu; remainder, Fe	525	12,000	4,000,000	Hard and brittle	Cast, grind

ening and finishing. Warpage during heat treatment, variations in dimensions, and changes in the value and distribution of the flux are sources of difficulty in this type of magnet. A magnet of this construction is shown in Figure 1C. While this type of magnet design affords low cost, the strength is usually somewhat less than that obtained with the solid magnet construction.

With this construction soft-iron pole pieces, although not always used, are desirable to insure permanence of the flux pattern in the armature gap, since any changes in flux pattern may affect the instrument accuracy.

From a mechanical standpoint, it is usually more difficult with built-up construction to maintain accurate and permanent alignment and relationship of the various parts of the element assembly than with the solid magnet construction.

Increased sensitivities of instruments were made possible by the use of cobalt alloy steels as permanent magnets. Pierre Weiss of Zurich, Switzerland, investigated this material in 1912, and it was later reduced to practice by P. H. Brace of the United States and Honda of Japan in an alloy having about three times the coercive force *H* and about equal to the residual *B* found in the chromium and tungsten steel alloys. For the past 15 to 20 years, this material has been used in instruments, providing a means of producing higher sensitivities. It has been used in forged and cast forms and is most attractive from the standpoint of high coercive force *H* of about 210 with total energy (*BH* maximum) of 900,000 but its inherent high cost limits its use primarily to the higher-sensitivity instruments where the chrome and tungsten steels are unsatisfactory.

The foregoing materials are all steels which require quenching in oil or water at high temperatures in order to develop properly their magnetic qualities.

Further phenomenal advances have been made in recent years in strength of permanent-magnet materials by the development of the aluminum-nickel-cobalt iron (Alnico) alloys which are of particular interest to the electrical-instrument designer because of their high values of coercive force and available energy as shown in Table I. These materials are characterized in electrical-instrument design by their relatively large cross section and short lengths of magnetic section to air-gap ratio, as has been previously shown,[2] and are highly resistant to demagnetizing fields because of their high coercive force *Hc*. While the Alnico materials have been used widely as instrument magnets, their manufacture is restricted to only two methods, namely, sintered process, and casting and grinding. The first process is usually restricted to relatively small designs sometimes found ideal for damping magnets and the like, as shown in Figure 2, while the second process is used for the production of the majority of Alnico magnets.

Cobalt-Molybdenum-Iron Alloys As Applied to D-C Instruments

The ideal magnet from the standpoint of the instrument designer would be one having high coercive force, residual induction, and available energy, and which had good machining and fabricating qualities. While a material of this type

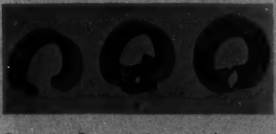


Figure 1. Forms of chrome-steel magnets

A. Formed and machined
B. Pole pieces added
C. Laminated construction

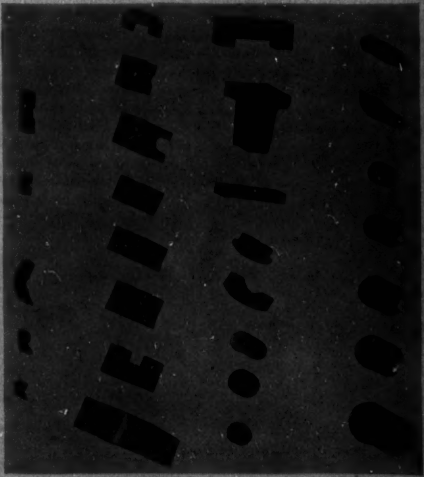


Figure 2. Sintered Alnico magnets

having magnetic characteristics equal to those of the Alnico group is not commercially available, an approach has been made in the cobalt-molybdenum-iron alloys more commonly known as Comol, as covered in Dean reissued United States patent 20800.

With the advent of Comol (typical composition: 12 per cent cobalt, 17 per cent molybdenum, remainder iron) a new material was made available which contains a minimum of the critical metals, such as cobalt, which could be cast easily and, when properly heat-treated, could be drilled, milled, and machined readily by conventional methods. As a result, accurate machining dimensions permit the degree of precision which is required to utilize this material fully in instrument magnets.

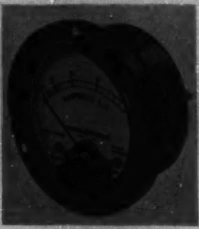


Figure 3. Small panel indicating instrument utilizing cast Comol magnet

A coercive force *Hc* of about 245 is obtained as compared with 210 for 36 per cent cobalt with a residual induction *Br* of 10,300, higher than either 36 per cent cobalt or Alnico II, and a maximum energy value of 1,100,000 as compared with 930,000 for 36 per cent cobalt and 1,650,000 for Alnico II.

A modification of the Comol magnet material described heretofore but containing appreciably lower percentage of the critical elements has been utilized for the first time in instrument application as the permanent magnet in a new "thin" line of d-c and radio-frequency General Electric small panel permanent-magnet-moving-coil-type instruments as shown in Figure 3.

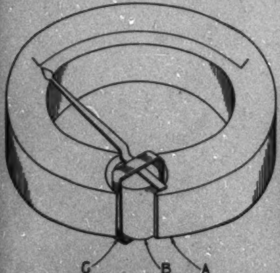


Figure 4. Basic element of permanent-magnet moving-coil instrument design

In view of the fact that very little information regarding the application of the newer permanent-magnet alloys as applied to indicating instruments is now available, it is proposed to treat this subject in some detail and to study the magnet circuit design as well as the moving system associated with it.

Permanent-magnet moving-coil d-c instruments of any conventional design contain certain essential parts as shown in Figure 4. *A* is the permanent magnet with suitably curved pole faces or with soft-iron pole pieces. *B* is a soft-iron cylindrical core centrally located with respect to the magnet pole faces or pole pieces so as to provide a uniform air gap in which the moving coil *C* turns upon its bearings.

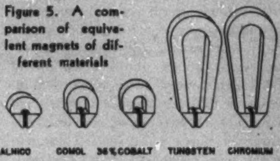


Figure 5. A comparison of equivalent magnets of different materials

The theory of operation of this type of design is well known and will be referred to only briefly at this time.

The permanent magnet *A* sets up a magnetic field in the uniform air gap, between the magnet pole faces or pole pieces and the cylindrical core *B*. The moving coil *C* is free to turn in the air gap and is equipped with two control springs, one fastened to each end of the coil, which serve to conduct the current to and from the coil and also control the pointer movement. The pointer movement is dependent upon the reaction between the flux due to the current in coil *C* and the flux due to the permanent magnet *A*. The turning moment or torque produced by the coil is directly proportional to the current passing through it assuming uniform flux density in the gap, and the opposing torque of the springs is proportional to the angle through which the armature turns, thus making the scale divisions upon which the value of current or voltage is measured, uniform.

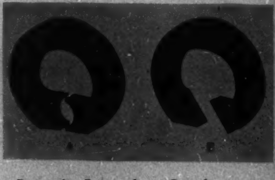


Figure 6. Forms of cast Comol magnets

A. Uniform gap
B. Diagonal pole face

From the preceding, it is seen that the permanent magnet is of utmost importance to the satisfactory operation of the instrument, and careful consideration should be given to its design.

The ideal condition in instrument magnet design from the standpoint of torque occurs when the magnetic energy maintained in the air gap is a maximum per unit volume of magnet steel. A comparison of ideal magnet sizes for a given strength of the more generally used magnet materials is shown in Figure 5. These ideal sizes are sometimes altered, however, to facilitate proper physical arrangement of other instrument components.

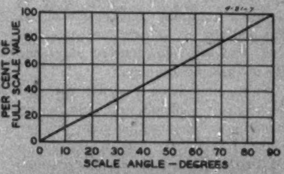


Figure 7. Typical scale distribution of indicating instrument using Comol magnet

The Manufacture of Cobalt–Molybdenum–Iron Alloy Magnets

A typical magnet of Comol material for the General Electric type *DW*51 instrument previously mentioned is shown

Figure 8. Element assembly utilizing cast Comol magnet

Figure 9. A method of magnetizing the element assembly

in Figure 6A. In general the manufacture of these magnets is as follows:

The Comol alloy is compounded in a high-frequency induction furnace using relatively pure raw materials and remelt scrap. It is cast in baked sand molds to form the particular magnet desired. Following this, the magnets are given a soaking treatment at a temperature close to their melting point to destroy the "as-cast structure" and develop the necessary conditions to permit age hardening later in the process. At the end of this treatment they are quenched rapidly and at this stage are relatively soft and easily machined. The magnets are drilled, milled, or ground, as required, and then age-hardened to develop the full magnetic properties.

For the so-called linear-scale thermocouple instruments, the diagonal pole-face magnet is used as shown in Figure 6B, and the casting and machining operations on it follow those of the uniform-gap magnet shown in Figure 6A.

This material has shown very uniform magnetic potential along the pole face so as to make the use of soft-iron pole pieces unnecessary. An actual distribution provided by this type of magnet is shown on Figure 7. The lack of soft-iron pole pieces has led also to slight reductions in leakage flux as compared with similar magnets of other materials utilizing soft-iron pole pieces. In this particular design the element is assembled completely to the unmagnetized magnet in a manner as shown in Figure 8. Such a procedure is desirable from the instrument manufacturer's standpoint, since it affords an ideal method of positioning the parts correctly and reduces the hazard of the magnet picking up extraneous particles of dirt during handling.

Magnetizing the magnets after assembly not only provides greater flux density but allows the magnet to be magnetized without a "keeper" and with the soft-iron core in place, thus giving a uniform magnetic potential along the pole face. This method eliminates the hazard of losing magnetic strength during assembly.

The ideal method of magnet saturation has been found to be by the use of a single conductor placed through the center of the magnet as shown in Figure 9 and by passing a high current through the conductor to saturate the magnet. Experience has shown that magnets of the size described herein require the following currents for approximately full saturation:

Material	Ampere Turns Per Inch
Chrome	500
Tungsten	600
36 per cent cobalt	1,700
Comol	2,000
Alnico II	4,400

The permanency of the magnet strength after final adjustment is important to the sustained accuracy of the instrument. While this is controllable to a large measure by the casting and aging technique, all magnets are demagnetized a few per cent in strength with an a-c field in order to stabilize them further. Changes in magnet strength with time on sample groups of magnets checked under laboratory controlled conditions have been negligible.

Changes in strength due to ambient-temperature changes on magnets made of the Comol alloy mentioned heretofore is approximately −0.017 per cent per degree centigrade rise, a value which is surprisingly close to values for ambient-temperature changes found in other magnet alloys.

Application of Comol-Magnet Material to a New Moving System

The moving system used with this new magnet is of the internal-pivot construction with the supporting pivots mounted inside the armature shell, whereas the conventional armature has the pivots mounted outside; see Figure 10. This feature has been used in order to obtain a thin element, and the conventional type of moving system can be used equally well with this magnet.

The general objective of the moving system design is to obtain a light weight with as low a moment of inertia as is practical for required stability and overload requirements, in order to realize the best over-all dynamic characteristics and a high mechanical factor of merit.

The mechanical factor of merit has been recognized as the engineering yardstick as to the practicability of a design from the standpoint of magnitude, or pivot friction, or zero uncertainty and sturdiness, or ability to withstand abnormal usage for a long period of time. This factor may be termed as an index of the freedom-from-friction errors and is a function of control spring opposing torque for a given angle deflection divided by the weight of the moving system. It has been shown previously[3,4] that, for the comparison of similar instruments of good commercial design having vertical shaft and V-type bearings this relation becomes:

Mechanical factor of merit $= T/W$ 1.5

where

T = spring torque in gram-millimeters for 90 degrees deflection

Table II. Constants for Typical $2^{1}/_{2}$-Inch-Diameter Small Panel-Type Comol and Chrome Magnet D-C Milliammeters, Accuracy ±2 Per Cent of Full-Scale Value

	Comol	Chrome
Magnetic Circuit		
1. Total mean magnet length (centimeters)	10.7	10.0
2. Air-gap area (square centimeters)	0.99	1.15
3. Air-gap flux (saturated) (maxwells)	1,980	1,495
4. Air-gap flux density (saturated) (gausses)	2,000	1,310
5. Torque per ampere turn K (gram-millimeters) (after adjusted)	1.38	0.905
Moving Element		
1. Mechanical factor of merit		
(a). For horizontal shaft (T/W)	0.486	0.318
(b). For vertical shaft ($T/W^{1.5}$)	0.915	0.598
2. Moment of inertia (gram-centimeter^2)	0.52	0.65
3. Damping factor (0−full-scale minimum)	9.4	3.6
4. Response (seconds) 0−2/3 scale	1.0	1.0
5. Ambient-temperature coefficient of magnet, per cent per degree centigrade	−0.017	−0.025
6. Maximum errors due to five gausses External field		
(a). Temporary error of per cent of full scale	1.0	1.0
(b). Permanent error of per cent of full scale	Negligible	Negligible
7. Magnet stability errors due to six cycles (four hours at −40 degrees centigrade, four hours at +75 degrees centigrade, four hours at +25 degrees centigrade)		
(a). Maximum change in per cent of full scale	0.10	0.52
(b). Minimum change of per cent of full scale	0.02	0.24
(c). Average change in per cent of full scale	0.07	0.35

W = weight of moving system and one control spring, in grams

For the comparison of similar instruments having horizontal shafts, this relation becomes T/W.

In good design, an effort is made to keep these factor-of-merit values relatively high which usually means high control-spring torque. The value of spring torque, other things being constant, varies as the magnet air-gap flux density as shown by the following:

The control-spring torque in the conventional permanent-magnet moving-coil design of instrument may be found from

$$T = KNI$$

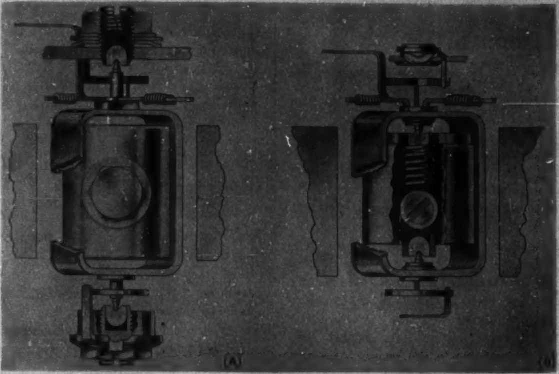
where

T = total control-spring torque in gram-millimeters for 90 degrees deflection
K = magnet constant in torque per ampere turn
N = number of turns on armature
I = current in amperes in coil

In order to keep the value of torque T as high as possible, the quantity KNI must be kept at a maximum. NI is kept as high as possible by using the maximum number of turns of wire for a given current sensitivity. Further increase in torque can be obtained only by increase in magnet strength, which makes any improvements in magnetic materials of prime importance to the instrument designer.

Consideration has to be given also to the minimum weight of the moving system. The "fixed parts" such as pointer, pivots, pivot supports, counterweight arms, and winding supports should be designed for minimum weight, but the winding will vary with sensitivity because of variation in number of turns and wire diameter used. In this particular design, the total weight of the winding will approximate twice the weight of the fixed parts for the higher-sensitivity designs and will approximate the weight of fixed parts for lower sensitivities.

A tabulation showing some of the more important constants of a typical $2^1/_2$-inch-diameter instrument using the high-coercive-force Comol cast magnet as compared with a conventional instrument of the same size using chrome steel formed magnet may be seen in Table II. The substantial increase in air-gap flux density by the use of Comol magnets provides a significant increase in the magnet constant K and the mechanical factor of merit and also improves the damping factor. With moving elements of about the same weight for the two designs and with increased magnet strength, the control-spring torque is increased, thus providing quicker response of pointer.



A. Conventional instrument element design

B. New element design, General Electric type DW52

Figure 10

Conclusions

The development of a new magnet material for use in the d-c small panel electrical instrument has been described and its characteristics compared with other materials heretofore used for the same applications.

The application of this material has provided better over-all characteristics by virtue of increased gap flux density and has provided a basis for the design of a sturdy line of shallow electrical indicating instruments having sustained accuracy and increased range of sensitivity.

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Testing Aircraft Generators

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Synopsis: The modern fighting airplane requires a large amount of electric power for its successful operation. The aircraft generator provides a reliable source of power at the lowest possible weight. The development of these generators has required close co-ordination of design, testing, and interpretation of test results. The tests include raw material, parts, laboratory, life, mechanical, and flight tests. This paper describes tests which have been made and the interpretations which have led to improvements in d-c aircraft generators.

THE engine-mounted aircraft generator, Figure 1, operates under conditions which are more severe than in any other type of service. The generator must carry its load over an ambient temperature range of more than 125 degrees centigrade and from sea level to an altitude of 30,000 feet or more, as shown in Figure 2. Under normal operating conditions the engine vibration may subject the generator to an acceleration as great as 40 to 50 times the acceleration due to gravity. The generator also must operate successfully over a speed range as high as 2.5 to 1.

Weight must be low. Thus, each mechanical part is designed for maximum strength and minimum weight. The electric conductors are operated at current densities as high as 10,000 amperes per square inch. No excess material can be permitted in the magnetic circuit. It is essential that each part of this circuit be operated at the highest permissible flux density.

Purpose of Testing

The aircraft generator could not have been developed to its present state without close co-ordination of design, testing, and interpretation of test results. Raw materials and parts are tested and inspected to insure high quality before they are built into the machine. Each generator is given a commercial test to demonstrate its ability to carry the required load without mechanical or electrical failure. Laboratory tests are made to improve quality and to eliminate troubles which might develop later. Improvements are being made continuously, but these improvements must be tested thoroughly before they are adopted. An attempt is being made constantly to reduce weight, but any lightweight design must be tested first to prove that the generator is adequate, mechanically and electrically.

Quality-Control Tests

The testing of aircraft generators begins with the raw materials and parts. Insulation must be proved adequate before it is used in an aircraft generator. Poor-quality electrical sheet steel cannot be tolerated. High loss in the laminations requires extra engine power and reduces the range of the plane. High loss also requires extra cooling air and reduces the rating of a machine of given size. Certain types of poor-quality electrical sheet steel will age in only a few hours time if operated at high flux density and temperature. This material must be avoided in aircraft generators.

Table I. Corrosion Tests of Aircraft-Generator Parts

Salt-Spray Tests

Part	Material	Finish	Hours Before Rusting	Duration of Test	Per Cent Surface Rusted at End of Test
Bracket	Steel	Zinc (light)	48	240	30
Bracket	Steel	Zinc (heavy)		264	None
Brush band	Copper alloy	Lacquer		240	No corrosion
Brush band	Steel	Zinc (medium)	240	264	Less than 1
Brush springs	Steel	Cadmium	240	240	Less than 1

The initial quality of electrical sheet steel can be determined with a fair degree of accuracy by means of the standard tests for determining losses and magnetic properties. The aging quality can be determined only by the life tests described later in this paper.

Better mechanical strength is required in the teeth of aircraft-generator-armature punchings than in the standard rotating machine. The teeth must withstand the vibration encountered on the engine in addition to the stresses imposed by the centrifugal forces produced by the high operating speed.

The mechanical strength of any material can be determined easily by standard testing equipment. Special test equipment is required often to predict the performance in the finished generator. Some of this special test equipment is described later in this paper.

Commutators

The commutator of an aircraft generator is an especially important part. No generator can operate successfully without a commutator that is stable, both thermally and mechanically. The dimensions of the component parts must be controlled closely, and the commutator must be seasoned properly. Each commutator undergoes several cycles of heating and tightening during its manufacture, so that there will be no bar movement after it has been built into an armature.

Commutators also are given a potential test before assembly in the armature. They are tested at 1,200 volts (rms) to ground and at 110 volts (rms) between bars. These tests are made at standard frequency. After the armature has been assembled it is tested at 700 volts to ground and is then given a high-frequency test to detect short-circuited coils. The armature is tested between brush positions at 750 volts and approximately 375 kilocycles (Figure 3).

Tests of Completed Machines

Each sample generator is subjected to a series of tests to determine its performance in all respects. A complete test on a sample generator consists of these items:

1. High potential, cold.
2. Neutral setting.
3. Brush run-in.
4. No-load saturation.
5. Load saturation.
6. Commutation at various loads.
7. Maximum load.
8. Overspeed.
9. High potential, hot, after overspeed.
10. Field-current check after overspeed.
11. Temperature test.
12. Losses and efficiency.
13. Ventilation.

Commutation is of great importance in d-c aircraft generators. Excessive

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Figure 1. 28.5-volt 200-ampere type *P*-1 aircraft generator

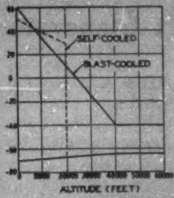


Figure 2. Temperature and altitude range of aircraft-generator operation

sparking causes short brush and commutator life and increased losses due to the circulating currents which are produced. Testing the preliminary sample permits the engineer to make the necessary adjustments and changes to improve commutation. The electrical performance of aircraft generators cannot be calculated nor predicted so accurately as that of large power machines. Changes after test are often necessary, but the engineer is guided by the tests in making changes. Commutation is improved frequently by a change in one or more of the following items:

1. Interpole ampere turns.
2. Interpole air gap.
3. Length of interpole.
4. Amount of compensation.
5. Neutral setting.
6. Grade of brush.

Production Tests

It is unnecessary to make complete tests on each machine that is built in daily production. Certain standard commercial tests will determine whether or not a generator will meet the expected performance. Each production generator should be given the following tests:

1. High potential, cold (500 volts rms).
2. Neutral setting.
3. Brush run-in.
4. Field current at minimum speed, maximum load, and rated voltage.
5. Overspeed test.
6. High potential, hot (500 volts rms).
7. Field-current check after overspeed test.

One production generator in approximately 20 should be given a full-load temperature test. This test should be made under standardized conditions. By comparison with previous tests any large change in losses can be detected. At regular intervals a complete test should be made, including separation of losses to provide a close check on quality of material.

Life Tests

Aircraft generators are different from standard machines, because the fullest use is made of every mechanical and electrical part. They cannot be designed for infinite life, because the weight would then become excessive. They are so designed that each part is worked to the utmost, and no part can have an excessive factor of safety. It is essential that some method be provided for determining any inherent weakness which might be present in the machine. Thus, life tests are made frequently on aircraft generators. The generator is connected to a driving motor of sufficient capacity and then loaded at its maximum rated load. The generator may be overloaded to determine the overload capacity.

Aircraft generators may fail either for mechanical or electrical reasons. The purpose of the life test is to discover the weakest part of the generator, so that the weakness may be corrected. For best results the life test must be run over a long period of time or until some part of the machine fails.

The commutation of the generator and the brush life can be studied best by means of the life test. Since the test is continued over a long period of time, the brush life can be determined accurately, and any change in the commutation can be observed. Thus, in conjunction with altitude tests, it is possible to select experimentally the grade of brush which will give the best performance over the life of the generator. The laboratory life test is conducted at sea level and cannot predict the performance at altitude. The brush life test must be co-ordinated with the altitude and flight tests described later.

The quality of insulation in a generator at the time of its manufacture is determined by the dielectric or high-potential test which is made during routine factory tests. These tests are no assurance that the insulation will not deteriorate and fail before the machine has operated for its expected life. The life test reproduces operating conditions and temperatures which are found in actual service. The use of the high-potential test at frequent intervals during the life test will demonstrate any weakness in the insulation.

The losses of poor-quality electric sheet steel will increase during the life of the generator. The high operating temperature of aircraft generators has an accelerating effect upon the normal increase in losses, caused by aging. Measurement of iron loss before and after the tests will show whether or not the iron has maintained its initial qualities.

Figure 3 (left). High-frequency test apparatus for armatures of aircraft generators

Figure 4 (right). Altitude chamber for testing aircraft generators

The electrical parts of an aircraft generator are designed to withstand a higher continuous operating temperature than the bearings. If the flow of air is obstructed, so that the bearings are not adequately cooled, failure will result. The life test, continued over a long period of time, will show up any defect in bearing design, manufacture, application, or ventilation.

Figure 5. Rotor of aircraft-generator brush tester

Figure 6. Aircraft-generator brush tester showing brushes in place

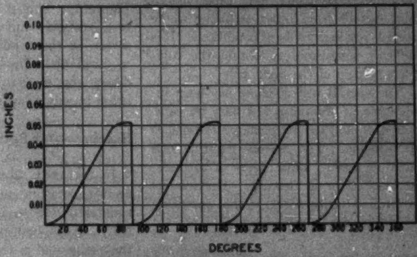


Figure 7. Curve showing contour of the rotor of brush tester

Figure 8. Fatigue-test arrangement for aircraft-generator brackets

Insulation

Except for commutation and voltage regulation, the ability of an aircraft generator to carry load depends on the temperature of the windings. Class-*A* insulation will withstand a temperature of 105 degrees centigrade for a period of several years. Class-*B* insulation will withstand a temperature of 125 degrees centigrade for a similar period. The two classes of insulation will withstand higher temperatures for shorter periods of time. Thus, in the life of an aircraft generator, class-*A* insulation can be operated at 130 degrees centigrade, and class-*B* insulation can be operated at 150 degrees centigrade without affecting the normal life of the machine.

The temperature of the windings of an aircraft generator should not exceed that for which they are designed. High temperature can be expected in the armature, so that class-*B* insulation should be used where needed. The highest temperature occurs between the two coil sides in a slot. Glass-insulated wire has been found to be best for armature conductors. A thin fish-paper cell often is suitable for the slot insulation, because the temperature is several degrees cooler than it is between the conductors.

No generator can operate for its maximum useful life without an adequate factor of safety in the insulation. The insulation must withstand the temperature to which it is subjected as well as maintain the required dielectric strength. The life test is the only successful way of determining whether or not the insulation will stand up. If the generator will withstand the initial test voltage after operating for a time equal to the expected life of the generator, the insulation can be considered adequate. Similar tests resulted in the adoption of the glass-covered wire mentioned in the preceding paragraph.

Ventilation

Most aircraft generators are forced-ventilated with the air passing axially through the generator. The quantity of air which passes through a 200-ampere 28.5-volt generator is approximately 75 cubic feet per minute at six inches of water, total pressure. Since the quantity is small, the air must be used effectively. All air passages should be designed so that the velocity is high. No passage should be permitted to short circuit any path where the air is needed for most effective cooling. Large openings through the rotor may prevent effective cooling of the stator.

Measurement of air pressure must be made at the generator. If measurement is made at a distance from the generator, the pressure drop between the point of measurement and the generator may be enough to affect seriously the accuracy of the test.

When an aircraft generator is installed on the engine, no avoidable obstruction should be permitted in the air passage leading to the generator. Back pressure in the engine nacelle reduces the amount of cooling air which the generator receives. Most aircraft generators are designed to deliver their rated output when cooling air is supplied at ten inches of water, total pressure. Usually not more than six inches is available at the generator. This reduces the maximum load which the generator can carry safely.

Engine Tests

The life test gives valuable data concerning the performance of an aircraft generator but does not duplicate the actual operating conditions encountered on a plane. The engine test often is used to reproduce more closely these operating conditions. The generator is mounted directly on an engine which drives a propeller. Thus, the generator must withstand approximately the same vibration that is encountered on an airplane.

An engine test is an excellent means of determining the ability of the generator to resist mechanical shocks and vibration. In addition, this test is a means of testing the performance of the gear unit through which the generator is driven.

Flight Test

No test has been found yet which will duplicate exactly the operating conditions that are found on an airplane. The final data as to how a generator performs must come from actual flight. These data may be obtained either from a special airplane used only for experimental purposes or from other airplanes where records are kept of flight conditions. It is difficult sometimes to obtain the complete history of the flight test.

The special test airplane has been invaluable in obtaining test data on commutation and the performance of brushes at altitude. The normal flight is one hour in ascent, three hours at altitude, and one hour in descent. The load is controlled during flight, so that operating conditions are known accurately.

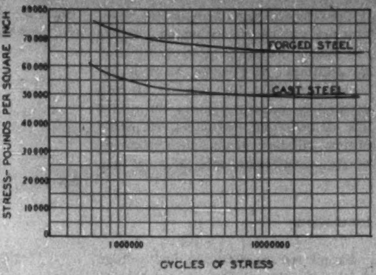
Altitude Chamber Tests

By means of the altitude chamber it has been possible to reproduce closely the conditions which are encountered when the airplane flies at an altitude of 30,000 feet or higher. A cooling system is provided, so that the low temperatures of altitude flying can be obtained. The low pressure within the chamber is maintained by means of a vacuum pump. A drive shaft extends through the wall of the chamber. Terminals are provided for electric connections. Any electrical condition which the generator encounters on an airplane can be reproduced closely in the altitude chamber. A small altitude chamber is shown in Figure 4.

Brush Tests (Mechanical)

The brushes of aircraft generators are subjected to severe mechanical operating conditions. Brushes have failed because of breakage, loose rivets, broken shunts, or frayed insulating sleeving. A vibration tester has been devised for testing brushes mechanically. The tester consists of two brush-holder assemblies mounted in a generator frame together with two commutators which have been machined, so that each brush drops approximately $^3/_{64}$ inch four times in one revolution. The tester shown in Figures 5 and 6 will test 16 brushes at one time. The contour of the rotor is shown in Figure 7. This is an extremely severe test, probably more than 100 times as severe as the condition encountered on an airplane. The vibration tester will detect any mechanical weakness in the brush in only a few hours operating time.

Figure 9. Fatigue tests of aircraft-generator brackets



Brackets and Bolts

In the early days of 200-ampere aircraft generators much trouble was encountered due to broken brackets and bolts. A die-cast bracket was used on the engine end of the generator, and this was unable to withstand the vibration and shocks encountered. Trouble was encountered also with breakage of the screws which are used to attach the bracket to the generator yoke. Extensive fatigue tests were made to determine the source of trouble and how the bracket design could be improved to eliminate the trouble. The test setup is shown in Figure 8. The curves shown in Figure 9 were obtained from these tests.

Corrosion

Aircraft generators are required to operate in any kind of climate. An open piece of electrical apparatus cannot be made entirely corrosion-proof, but the parts should be made so that they will resist the weather conditions commonly encountered. To establish their ability to resist corrosion, a series of tests was made on aircraft-generator parts. These results are shown in Table I.

As a further check, outdoor corrosion tests were made on two *P*-1 generators completely assembled. One generator was placed under a shelter, open at the sides. The other generator was exposed completely with no protection. The generators were mounted horizontally with the air connection downward. They were taken from the assembly line and were given no additional treatment. The generators were exposed for four weeks during the past summer. Rain fell several times during the test.

At the end of four weeks the only evidence of corrosion was a slight amount of rust on the steel parts inside the generator and on the spline. The insulation resistance varied from day to day, but at the end of the test it was as high as it was at the beginning. There was no apparent difference in the appearance of the two generators at the end of the test, although one had been under a cover, and the other had been exposed entirely. The paint was in good condition at the end of this test.

Conclusions

The test program for aircraft generators has required much testing of generator parts as well as of complete machines. The object of all of these tests has been to build the most reliable generator with the lowest possible weight. The test program has been responsible for the correction of the troubles which have developed in the field and for the elimination of potential sources of trouble. The test program is not complete and never will be complete so long as aircraft generators are built. The life test has been responsible for a great amount of improvement in aircraft generators. By means of these tests in which the generator carries a definite load, the life of insulation has been studied. It has been possible to apply the insulation, so that each electrical part is insulated adequately. Class-*A* material has been used where it is adequate for the operating temperatures encountered in aircraft generators. Class-*B* insulation has been used where required because of higher temperatures, as in the armature conductors. The insulating material has been chosen also, so that the generator will withstand the potential tests with an adequate margin.

Potential Breakdown of Small Gaps Under Simulated High-Altitude Conditions

M. J. DeLERNO
ASSOCIATE AIEE

CONCURRENT with the present development program of a 400-cycle 208-volt power supply for aircraft, it was deemed advisable to study the effect of high altitudes on the breakdown potential between various conductors at different voltage levels.

Aircraft electrical equipment will be subjected to severe and unusual conditions which will affect materially high potential specifications. Some of the most severe conditions are:

(*a*). Airplanes may be parked on the ground for several weeks without seeing use. In equatorial regions the moisture content of the air may be very high and may even reach 100 per cent. Operation of equipment under such humid conditions may affect breakdown potential values.

(*b*). Airplanes will be flying part of the time at high altitudes (40,000 feet) and part of the time at lower altitudes. At the high altitudes all equipment not heated or located in a heated section of the plane will get very cold. When the plane descends to lower altitudes, the humidity and temperature will be higher, and moisture will probably condense on the cold equipment, first as frost and later as moisture.

(*c*). In many cases airplanes are forced to fly from dusty fields or through dust storms. Any exposed surfaces may be contaminated by dust.

Scope of Investigation

The breakdown potential is that potential which causes complete breakdown or flashover between the electrodes. It is defined as the potential which will produce a luminous discharge between the electrodes. This discharge may clear itself immediately or may be followed by an arc, depending upon the power capacity and the impedance of the electric circuit. The chief characteristic of an arc discharge is the low voltage drop between the electrodes.

In order to simulate true high-altitude conditions, it is necessary to reduce both the pressure and temperature of the air which surrounds the test specimen to the same degree which exists at any specified altitude. However, several devices in airplanes are located near pieces of equipment which operate at high temperatures and which consequently heat the air in the vicinity. These devices therefore may be surrounded by an atmosphere which is at the same pressure but at higher temperature than the air which normally exists at that altitude. It will be shown from theoretical considerations that, for any given gas pressure and electrode spacing encountered on aircraft equipment, the breakdown potential usually decreases with an increase in temperature. Since higher temperatures decrease the breakdown potentials, it was thought best to perform all tests at reduced pressures corresponding to any given altitude but to leave the air temperature fixed at its sea-level value.

In production testing of 400-cycle equipment to determine dielectric defects, a source of 400-cycle power is not usually available, and it is therefore necessary to use 60-cycle supply. A comparison between breakdown at 400 cycles and 60 cycles under similar conditions was undertaken to establish a relationship between them. This was also desirable since breakdown data which had been taken previously with 60-cycle supply could be converted to equivalent 400-cycle data.

The two types of breakdown affected by high altitudes are air-gap breakdown and surface or creepage breakdown. Designers of low-voltage electrical control equipment sometimes use air-gap breakdown values between spheres and creepage breakdown values in a uniform

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Improvements in brush life have depended mostly on generator life tests. By co-ordinating the life tests at sea level with the data obtained from the altitude chamber and flight tests, the best possible brush has been obtained. A brush has been developed which has satisfactory life both at sea level and at altitude. The subject of brush development is too broad to be more than mentioned in this paper.

The tests which have been made in the vibration tester shown in Figures 5 and 6 have enabled the mechanical development of brushes to keep ahead of the requirements in the airplane. The rarity of mechanical brush failure can be attributed to improvements which have been made as a result of tests made in the vibration tester.

The altitude chamber has not been used long enough to determine its fullest possibilities for testing generators. The tests which have been made already show that altitude conditions can be reproduced very closely. The chamber tests in conjunction with the life test at sea level will facilitate the improvement of brushes and commutation.

The mechanical forces encountered in engine tests are nearly the same as those encountered in flight. These tests have been of great value in determining the adequacy of brackets, bearings, connectors, leads, and terminals.

The flight test permits a close check to be made on the test data obtained in the altitude chamber. Brush improvements which have been made as a result of altitude chamber tests must be given the final test in flight. Flight tests cannot be made so easily nor so often as tests in the altitude chamber but are of great value, because the generator encounters exactly the same conditions that are found in actual service. In addition to brush tests, flight tests like engine tests have served to test the ability of brackets, bearings, leads, and connectors to stand up in service.

Laboratory tests of generator parts have enabled the engineers to improve these parts; thereby many sources of trouble have been eliminated. No untried part can be used on an aircraft generator. Laboratory tests must be relied on for a preliminary check of all improvements before they are subjected to engine or flight tests.

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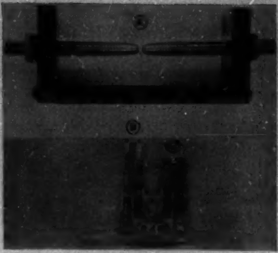

Figure 1. Air-gap breakdown specimens

A. 1/4-inch-diameter brass hemispheres

B. Parallel straight silver-plated phosphor-bronze strips, 0.010 inch thick. Air gap, 0.187 inch

C. Parallel L-shaped silver-plated phosphor-bronze strips, 0.031 inch thick. Air gap, 0.117 inch

electrostatic field as a preliminary basis for determining minimum clearances on new designs (when not specified in an AIEE or similar standard). At best, conclusions obtained in this manner can approximate only roughly actual conditions on aircraft equipment, for the potential field produced between various aircraft parts is quite different from that which exists between spheres or between parallel plates (uniform field). Therefore, air-gap and creepage breakdown potentials were taken between various parts of aircraft relays and contactors in addition to tests taken on hemisphere gaps and uniform-field specimens.

In order to reproduce conditions which may exist in part *b* of the introduction, several aircraft relay parts were cooled to 0 degrees Fahrenheit and were then placed in a chamber which was maintained at about 85 per cent humidity. Voltage was applied between current-carrying parts of the specimen shortly after introduction into the chamber.

Test Equipment

The mechanical equipment used in performing the tests consisted of a bell jar in which the tests were conducted, a rotary vacuum pump for evacuating the bell jar, a thermometer, a humidity meter, a container of calcium chloride for drying the air, and a jar of water for raising the humidity of the air. Electrical connections to the test specimen were made through studs at the top of the bell jar.

The electric circuit consisted of a step-up transformer supplied by either a 60-cycle or a 400-cycle alternator. A series resistor was inserted between the alternator and transformer to limit the discharge current after breakdown occurred. A foot switch was also located in the transformer primary circuit to interrupt the circuit when breakdown occurred. In this way it was possible to use the same specimen for a large number of tests before carbonization of the specimen occurred. The breakdown potential was measured with a step-down potential transformer and a voltmeter. The wave form of the voltage across the power transformer was not a true sine wave because the alternator voltage did not have a true sinusoidal wave form. Therefore the voltages measured with the potential transformer were calibrated with voltages measured across the test specimen with a crest reading voltmeter.

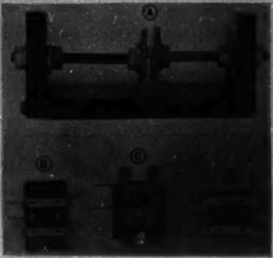

Figure 2. Creepage breakdown specimens

A. Uniform field specimen

B. Textolite, 0.12-inch spacing

C. Textolite, 0.035-inch spacing

D. Hot-molded compound, 0.13-inch spacing

The test specimens are shown for air-gap breakdown in Figure 1 and for creepage breakdown in Figure 2. All specimens, except hemispheres and the uniform-field specimens, were obtained from standard aircraft relay parts and will simulate actual conditions which exist on aircraft.

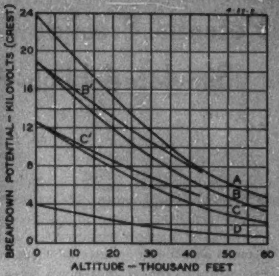

Figure 3. Hemisphere-gap breakdown characteristics

1/4-inch-diameter brass electrodes

A. 1/2-inch gap. Air temperature 72 degrees Fahrenheit

B. 1/4-inch gap. Air temperature 72 degrees Fahrenheit

B'. 1/4-inch gap. Air temperature corresponds to various altitudes (calculated)

C. 1/8-inch gap. Air temperature 72 degrees Fahrenheit

C'. 1/8-inch gap. Air temperature corresponds to various altitudes (calculated)

D. 1/16-inch gap. Air temperature 72 degrees Fahrenheit

Summary of Data

Table I shows the per cent difference between the average breakdown potential (average of ten readings) for 60 cycles with that for 400 cycles at 72 degrees Fahrenheit for reduced pressures corresponding to various altitudes. Although there are a few cases in the table in which the difference exceeds ten per cent, it should be pointed out that variations in

Table I. Comparison Between Breakdown Potentials at 60 and at 400 Cycles

Specimen	Specimen Number	Electrode Spacing (Inches)	Per Cent Difference $\frac{V(60) - V(400)}{V(60)} \times 100$ Altitude (Feet)* 0	15,100	30,300	45,200
Air-Gap Breakdown						
Parallel straight relay strips, Figure 1B	1	0.187	2.9	−3.8	3.8	−3.7
	2	0.187	0.4			9.8
Parallel L-shaped relay strips, Figure 1C	1	0.117	−2.0	7.9	4.8	7.7
	2	0.117	0.6			−5.0
Creepage Breakdown (Nonuniform Field)						
Textolite, Figure 2B	1	0.125	0.3	8.0	0	−13.0
	2	0.125	−0.3	0.9	− 0.6	−19.1
	3	0.125	0.3			−24.6
	4	0.125	1.8			−13.3
Textolite, Figure 2C	1	0.035	−8.2	−8.7	−10.4	1.4
	2	0.035	−3.0	−2.3	− 1.0	5.1
	3	0.035	1.9			− 2.6
	4	0.035	5.4			2.7

* Corresponds to pressures existing at the specified altitude, but not to temperatures. Temperature held at 72 degrees Fahrenheit.

Table II. Effect of High Humidity on the Creepage Breakdown Potential of Precooled Specimens

Specimen	Specimen Number	Electrode Spacing (Inches)	Number of Tests	Per Cent Difference: $\frac{\text{Min (A)*} - \text{Min (B)**}}{\text{Min (A)}} \times 100$, Altitude (Feet)† 0	$\frac{\text{Min (A)*} - \text{Min (B)**}}{\text{Min (A)}} \times 100$, Altitude (Feet)† 45,200	$\frac{\text{Avg (A)*} - \text{Avg (B)**}}{\text{Avg (A)}} \times 100$, Altitude (Feet)† 0	$\frac{\text{Avg (A)*} - \text{Avg (B)**}}{\text{Avg (A)}} \times 100$, Altitude (Feet)† 45,200
Textolite, Figure 2B	1	0.125	3	25.3	28.6	16.0	2.0
	2	0.125	2	32.1	18.8	20.2	15.5
	3	0.125	1	22.0		− 1.4	
	4	0.125	1	21.8		10.2	

* (A) Breakdown potential of a dry specimen.

** (B) Breakdown potential of a specimen subjected to special moisture conditions.

† Corresponds to pressures existing at the specified altitude, but not to temperatures. Temperature held at 72 degrees Fahrenheit.

the average high potential breakdown at 60 cycles for different specimens having the same dimensions and constructed of the same materials have exceeded 25 per cent. Since 64 per cent of the differences are less than ±five per cent and 86 per cent less than ±ten per cent, it seems justifiable to conclude that there is no appreciable difference between 60- and 400-cycle high-potential breakdown values for the range of electrode spacings used.

Similar conclusions may be drawn on the effect of humidity on high-potential air-gap and creepage breakdown values. A comparison between tests performed with 20 per cent relative humidity and with 85 per cent relative humidity at 72 degrees Fahrenheit for reduced pressures corresponding to various altitudes showed that 50 per cent of the differences between the average breakdown values (average of ten readings) at 20 per cent humidity and 85 per cent humidity, divided by the average breakdown values at 20 per cent humidity are less than ±five per cent, and 80 per cent are less than ±ten per cent. These results indicate that humidity has inappreciable effect on air-gap breakdown potential characteristics and on creepage breakdown potential characteristics for the materials tested.

Tests which were conducted on specimens which had been cooled to 0 degrees Fahrenheit and then placed in a high humidity atmosphere (85 per cent) at 72 degrees Fahrenheit showed that moisture condensed on the surfaces of the specimens. The moisture lowered the initial creepage breakdown potential of a specimen, but successive breakdowns dried the surface sufficiently to raise the breakdown potential to its average dry value. It should be pointed out that if the electric circuit is such that an arc can follow the initial spark, then the surface of the insulation is usually carbonized by the arc to such an extent that a permanent short circuit is created between the electrodes. Since repeated flashovers raise the breakdown potential nearly to the dry breakdown potential, then the per cent difference between the minimum breakdown potential of a dry specimen and the minimum breakdown potential of the same specimen when treated as already stated is more important than the per cent difference between the average values of the dry specimen and the treated specimen. Table II shows the per cent difference for both the minimum and average values. Ten readings were obtained for each test, from which the minimum and average values were obtained. Sometimes more than one test was performed on a specimen, and, when such was the case, the minimum and average values were obtained from the total number of readings. It was found from experimental tests that the initial breakdown potential of ceramic specimens when subjected to this special moisture condition could be raised about 25 per cent by treating the ceramic with the vapors of silicon methyl chloride (a silicone derivative). This treatment produces a ceramic surface which is water repellant and which has a very high electrical surface resistivity under dew-point conditions.

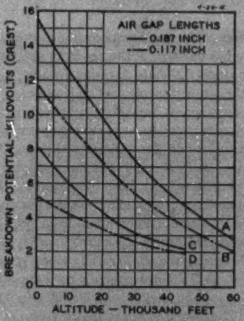

Figure 4. Comparison of air-gap breakdown between parallel strips with air-gap breakdown between hemispheres

A-B. 1/4-inch-diameter brass hemispheres. Air temperature 72 degrees Fahrenheit

C. Parallel straight silver-plated phosphor-bronze strips, 0.010 inch thick. Air temperature 72 degrees Fahrenheit

D. Parallel L-shaped silver-plated phosphor-bronze strips, 0.031 inch thick. Air temperature 72 degrees Fahrenheit

(See Figure 1 for picture of specimens)

Breakdown potential values under various conditions are shown in Figures 3, 4, and 5. Figure 3 shows the hemisphere-gap breakdown characteristics for different electrode spacings. It also shows the effect of reduced temperatures on the breakdown characteristics based on theoretical analysis. Figure 4 compares the air-gap breakdown characteristics between hemispheres with the air-gap breakdown characteristics between various aircraft relay parts. Figure 5 makes a similar comparison between creepage breakdown in a uniform and nonuniform electrostatic field.

From curves similar to Figures 3, 4, and 5, a table showing the ratio of breakdown potential at sea level to a pressure corresponding to 45,000 feet elevation for an air temperature of 72 degrees Fahrenheit for both air-gap and creepage breakdown has been compiled. Table III shows this ratio. This ratio is important in determining equivalent sea-level high-potential voltage values to correspond to altitude high-potential values. If the AIEE specification requiring electrical equipment to withstand a high-potential

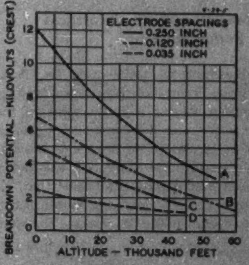

Figure 5. Comparison between creepage breakdown in a uniform electrostatic field and creepage breakdown in a nonuniform electrostatic field

A-B. Breakdown across Textolite surface located in a uniform field. Air temperature 72 degrees Fahrenheit

C-D. Breakdown across Textolite surface located in a nonuniform field. Air temperature 72 degrees Fahrenheit

(See Figure 2 for picture of specimens)

Table III. Ratio of Breakdown Potential at Sea Level to 45,000 Feet*

	Ratio V(0) to V(45,000)
Air-Gap Breakdown	
Hemisphere gap, $^1/_{32}$-inch spacing	3.5
Hemisphere gap, $^1/_8$-inch spacing	3.4
Hemisphere gap, $^1/_4$-inch spacing	3.3
Hemisphere gap, $^1/_2$-inch spacing	3.3
Parallel straight strips—0.187-inch gap	3.7
Parallel L-shaped strips—0.117-inch gap	2.7
Creepage Breakdown	
Uniform field	
Textolite, $^1/_8$-inch thick (arc with grain)	3.4
Textolite, $^1/_4$-inch thick (arc with grain)	3.0
Textolite, $^1/_8$-inch thick (arc across grain)	3.0
Textolite, $^1/_4$-inch thick (arc across grain)	2.8
Cold-molded asbestos compound, $^1/_8$-inch thick	2.8
Cold-molded asbestos compound, $^1/_4$-inch thick	3.0
Nonuniform field	
Hot-molded compound (Bakelite material 1182) 0.13-inch spacing	3.5
Textolite, 0.125-inch spacing	3.1
Textolite, 0.085-inch spacing	2.1

* Corresponds to pressures existing at the specified altitude, but not to temperature. Temperature held at 72 degrees Fahrenheit.

voltage of twice rated voltage plus 1,000 volts is to apply at 45,000 feet, then the same equipment will have to withstand from 2.7 to 3.7 times as high a voltage at sea level. On 208-volt circuits, the equipment would have to withstand 1,416 volts at 45,000 feet and from 3,820 to 5,240 volts at sea level. Such a specification is rather severe under such conditions, and it would seem that a more satisfactory specification would be to require an equivalent sea-level voltage of 2.7 to 3.7 times twice rated voltage plus an additional 1,000 volts. On 208-volt equipment, the equivalent sea-level voltage would then be from 2,122 to 2,540 volts.

Comparison With Theoretical Performance

Experimental tests made by Peek[1] indicate that the breakdown voltage between spheres is independent of the frequency, provided corona is not present before breakdown to provide a considerable space-charge effect. He also determined from experimental tests[2] that, for spheres, corona does not appear before breakdown until the spacing between the spheres equals eight times the radius of the sphere. The maximum spacing-radius ratio for the hemispheres was four ($^1/_2$-inch gap, $^1/_8$-inch radius). The theory, therefore, seems to check the data that breakdown characteristics for hemisphere gaps are the same at 60 and 400 cycles.

The effect of humidity on air-gap breakdown for sphere gaps is inappreciable if the surfaces remain dry,[3] while the effect of humidity on needle gaps is to require a higher voltage to cause breakdown when the humidity is high than when it is low.[4] These two conditions correspond somewhat to the hemisphere gap and the air gap between various parts of relays, respectively. However, no humidity effect was observed on gaps between various relay parts.

In order to check the effect of reduced temperatures on the breakdown characteristics of air gaps, use was made of the principle of similitude.[5] This principle, which was developed by Townsend, states that, for geometrically similar systems, the breakdown potential is a function of only the product of gas density and one of the linear dimensions of the system, provided no space-charge effect is present before breakdown. Since the gas density is directly proportional to the gas pressure and inversely proportional to the gas temperature, then an equation expressing these statements will be

$$E=f(\rho\times d)=f_1\left(\frac{P}{T}\times d\right)$$

where E is the breakdown potential, ρ the gas density, T the absolute gas temperature, P the absolute gas pressure, and d the linear dimension of the system.

To convert breakdown characteristics at room temperatures to temperatures corresponding to various altitudes, the value of P/T must remain constant if E and d are held fixed. By reading the value of E corresponding to some pressure and 72 degrees Fahrenheit from the experimental curves of Figures 3 and 4, and calculating the P/T ratio, a new value of P and T corresponding to some altitude can be picked which will give the same P/T ratio. The value of E will then apply to the new temperature and pressure conditions which are the true conditions existing at that altitude. Figure 3 shows curves calculated in this manner.

Conclusions

The following conclusions may be drawn from the data presented:

1. Air-gap and creepage breakdown characteristics are substantially the same for 60- and 400-cycle supply for short gaps.

2. The effect of a very humid atmosphere on air-gap and creepage breakdown potentials is inappreciable, provided there is no moisture condensation on the electrodes or the dielectric.

3. About 25 per cent reduction in the initial creepage breakdown potential can be expected if there is appreciable moisture condensation on the surface of the dielectric. Succeeding breakdown values will approach gradually the flashover value for the dry dielectric, provided the surface does not become carbonized.

4. Treating ceramic parts with a silicone vapor will raise the initial creepage breakdown about 25 per cent if there is considerable moisture condensation on the surface of the ceramic.

5. Breakdown characteristics obtained on aircraft relay and contactor parts may differ as much as 100 per cent from characteristics obtained from sphere-gap observations for air-gap breakdown or from uniform-field breakdown observations for creepage breakdown.

6. The ratio of air-gap or creepage breakdown potential at sea level to pressures corresponding to 45,000 feet varies from 2.7 to 3.7, depending upon the electrode shape and spacing for air-gap breakdown, and on the dielectric material and the electrostatic field distribution betweeen the electrodes for creepage breakdown.

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High-Voltage Oil Circuit Breakers for Rapid Reclosing Duty

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Synopsis: Experience has shown that high-speed reclosing of circuit breakers on high-voltage transmission lines is of considerable value in maintaining system stability and continuity of service. This paper describes a new line of outdoor oil circuit breakers and the special design features which make them suitable for 20- and 35-cycle reclosing duty at voltages as high as 230 kv. Data are included showing high-power-laboratory short-circuit tests on these duty cycles at 138 kv and 230 kv at currents corresponding to three-phase duty of more than 2,500,000 kva.

THE present emergency demands the highest possible reliability in the electrical systems which are supplying unprecedented loads to industrial centers. In many locations the use of high-speed reclosing of high-voltage circuit breakers has proved to be of great value in maintaining continuity of service. System-stability requirements have brought about the development of high-speed circuit breakers in the voltage range from 115 kv to 287 kv with an interrupting time of five or even three cycles. In recent years, a few of these high-speed breakers have been available with provision for high-speed reclosing. This paper describes the design and performance of a line of high-voltage circuit breakers particularly designed to meet these severe operating requirements.

Because of the arc-interrupting effectiveness and low arc energy of multiflow deion grid contacts described in a paper before the Institute a year ago,[1] it is possible for the physical size of these high-voltage reclosing circuit breakers to be smaller than the older standard breakers. Simplification of the operating mechanism is made possible by utilizing the recently developed compressed-air type of mechanism for operating high-voltage oil circuit breakers.[2] Short-circuit tests made in the high-power laboratory have demonstrated the ability of these new circuit breakers to interrupt on either a 35-cycle or a 20-cycle reclosing duty cycle short-circuit powers as high as 2,500,000 kva.

The new interrupter for these oil circuit breakers, called the multiflow deion grid, is smaller in size than earlier forms of contacts. From the nature of its operation the arcs are kept under control at all times, and the evolution of gas is so reduced that very little space is needed for insulation around the grid. The live parts are compactly enclosed within well-rounded metallic shields so that the voltage gradient through the oil is low enough to allow the most effective use of well-co-ordinated striking and creepage distances. For still higher interrupting capacities which are necessary to meet present-day requirements, such as 3,500,000 kva, interrupting units built with a greater degree of ruggedness perform entirely satisfactorily.

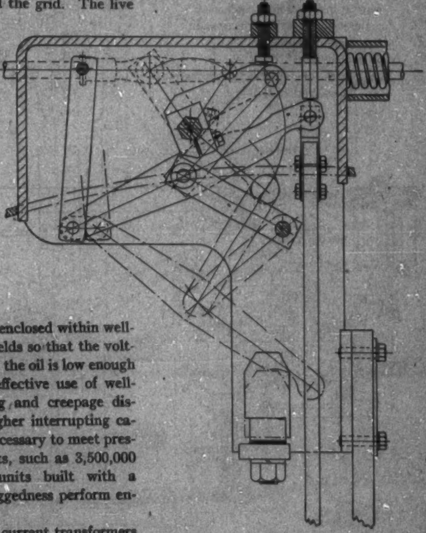

Figure 1. Pole-unit lever system for 138-kv breaker, showing relation of toggles, stops, dashpot, and accelerating spring

The bushing-type current transformers are built to modern American Standards Association and National Electrical Manufacturers Association standards. The standard design of one current transformer per bushing fulfills usual requirements, although ample space can be made available to provide for two transformers per bushing where required, either multiratio on the Hipersil core for relaying purposes or single ratio on the Hipernik core for high-accuracy metering service. The small bushing diameter is also an advantage here, as it permits plenty of space for the transformers without making necessary the use of protruding casings to mar the otherwise smooth-walled tank.

The new line of breakers presented an opportunity to make a new design of pole-unit levers, better adapted to high-speed operation. The general features required for straight line motion which have been used for so many years have been retained, but, with the more effective interrupters, it is now possible to reduce the length of travel and yet keep ample toggle effect at the closed position of the contacts. Fabricated steel levers and links are used throughout as shown in Figure 1. The load characteristic (Figure 2) gives the various increments of load for the entire closing stroke, plotted against mechanism pull-rod travel. In other words, each static load inside the three tanks, such as the dead weight of the contacts and levers, and the contact-spring load is multiplied by the pull ratio, to convert it into equivalent load at the operating mechanism. Since the accelerating spring for the mechanism illustrated is outside the tank, it is added without being modified by the pull ratio. A summation of these increments shows a net load increasing from slightly over 1,000 pounds at the open position, to a little over 3,000 pounds at the closed position, all of which is well within the ability of either the conventional solenoid or pneumatic mecha-

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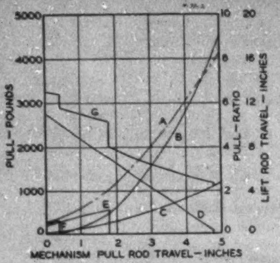


Figure 2. Load curve for three-pole 138-kv breaker

A. Pull ratio of levers showing greatest mechanical advantage at closed position of stroke

B. Relation of lift-rod travel to operating-mechanism travel

C. Equivalent weight of pole-unit levers, contacts, and so forth, expressed as equivalent mass at the operating mechanism

D. Accelerating spring load in pounds

E. Intermediate contact load

F. Main contact load

G. Total load on operating mechanism. Summation of *C-D-E-F*

nism. In this class of breaker the added load due to forces encountered in closing against fault currents is negligible and hence can be neglected.

Compressed-Air Operating Mechanism

A schematic view of the compressed-air operating mechanism, suitable for high-speed reclosing, is illustrated in Figure 3. The operating system is connected at all times to the main-breaker operating rod, only one lever being required for latching purposes. During the closing operation, air is admitted to the top of the cylinder, forcing the piston down to the closed position where it is held by the latch. Energizing the trip coil also operates a large "dump valve" arranged so that any air left in the cylinder on the top side, as for example on immediate tripout after closing, will be exhausted sufficiently rapidly to give full opening speed. The acceleration on opening is provided entirely by springs so that even in the event of failure of the compressed-air system, the breaker could perform its protective function of clearing the circuit. To obtain rapid reclosing it is necessary to time the opening of the valve admitting air to the cylinder such that the breaker will be brought to rest before the normal full-open position but with ample margin for interrupting the arc, and returned to the closed position within the required time. A record of a timing test showing an open–close–open operation is illustrated in Figure 4. It is to be noted that the velocity of contact opening is essentially the same on the second tripping as on the first, illustrating the effectiveness of the dump valve in giving pneumatically trip-free operation as an alternative to mechanically trip-free operation.

Multiflow Deion Grids for High-Speed Reclosing

Oil circuit breakers used on automatic-reclosing duty cycles are subject to certain standard derating factors according to the number of unit operations or groups of unit operations. For instance, a circuit breaker having an interrupting capacity between 5,000 and 10,000 amperes on the standard two-CO–15-second duty cycle (two-unit operation) will have a rating of only 85 per cent when the two-unit operations follow one

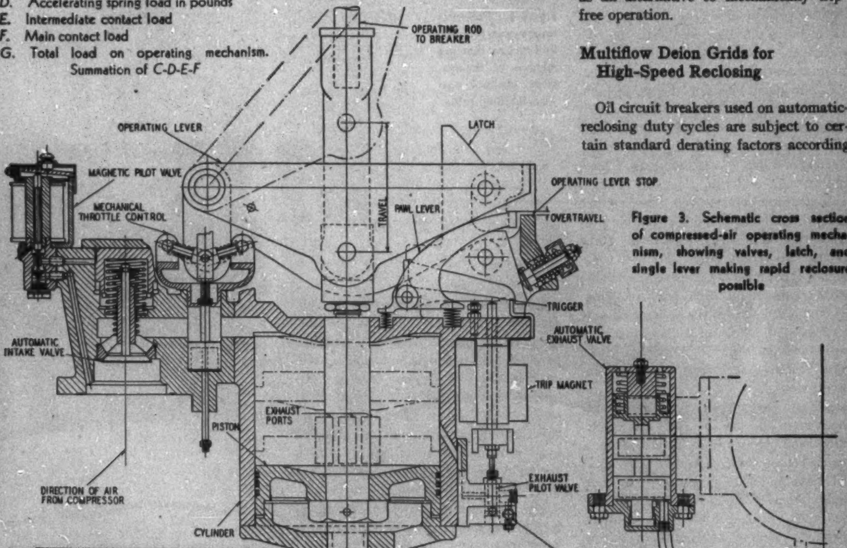


Figure 3. Schematic cross section of compressed-air operating mechanism, showing valves, latch, and single lever making rapid reclosure possible

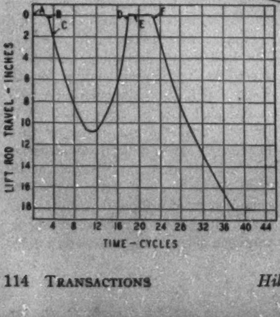


Figure 4 (left). Record of oscillogram showing time in cycles after trip coil is energized at *A* till at *B* contacts part, at C arc is interrupted, breaker recloses after partial stroke, and contacts touch at *D*. At *E* the trip coil is again energized, and contacts part in normal time at *F*

Table I. 230-Kv Three-Cycle Breaker Test (Two Grids Per Pole)

(a). One Grid, 1/2 Pole, 66 Kv to Ground, Charging Current, 60 Cycles

Test Number	Interrupted Current (Amperes)	Arcing Time (Cycles)	Main Gap Per Grid (Inches)	*Interrupting Time (Cycles)
1	45	1.0	2.1	2.4
2	45	1.1	2.45	2.5
3	45	1.2	2.8	2.6
4	45	1.3	3.15	2.7
5	45	1.4	3.5	2.8
6	45	1.2	2.8	2.6
7	90	1.6	4.2	3.0
8	90	1.5	3.85	2.9
9	90	1.15	2.6	2.5
10	90	1.3	3.15	2.7
11	90	1.5	3.85	2.9
12	90	1.5	3.85	2.9
13	180	1.3	3.15	2.7
14	180	1.0	2.1	2.4
15	180	1.1	2.45	2.5
16	180	1.4	3.5	2.8
17	180	1.2	2.8	2.6
18	180	1.4	3.5	2.3

(b). Two Grids, 132 Kv to Ground, Charging Current, 60 Cycles

Test Number	Interrupted Current (Amperes)	Arcing Time (Cycles)	Main Gap Per Grid (Inches)	*Interrupting Time (Cycles)
1	90	1.4	3.5	2.8
2	90	1.4	3.5	2.8
3	90	1.3	3.15	2.7
4	90	1.0	2.1	2.4
5	90	1.2	2.8	2.6
6	90	1.0	2.1	2.4

* Based on time of 1.4 cycles from energizing trip coil to parting of contacts.

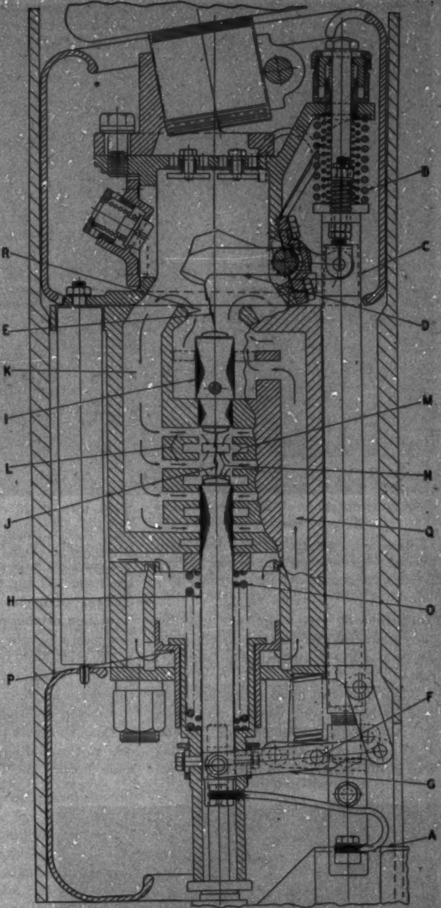


Figure 5. 230-kv multiflow deion grid for three-cycle operating time and 2,500,000-kva rupturing capacity

Tripping of breaker releases main cross-arm *A*, allowing springs *B* to move operating-rod *C* down and to rotate contact arm *D*, drawing pressure-generating arc *E*. Simultaneously, pin *F* swings lever *G* downward, pulling contact *H* away from intermediate contact *I* to draw main arc *J*. Pressure from upper arc *E* drives oil through channel *K* to multiflow inlets *L*, deionizing arc in multiorifice structure *M* and sending arc products out vents *N*. Springs *O* push piston *P* downward to provide supplementary oil flow for low-current arc interruption, flowing oil through channel *Q* to orifice *R* in upper gap and then to main flow channel. Back pressure from high-current arcs stops piston until arc is interrupted, when completion of stroke sends flushing oil flow through grid

another with no intentional time delay. These derating factors have been standardized in an attempt to take into consideration certain residual effects, such as temperature of the contact, accumulation of gas and carbon which may make the second interruption more difficult than the first. It is obvious that these residual effects will be minimized in proportion as the arcing time and arc energy are reduced. This result is accomplished in two ways by the multiflow-deion grid type of circuit interrupter. First, the pressure-generating arc and the interrupting arc in the higher-voltage breakers are drawn simultaneously, and second, the arc is deionized, without unnecessary lengthening, by a multiplicity of radially inflowing streams of oil. Figure 5 shows a cross section of the latest design of multiflow deion grid for three-cycle operation at 230 kv. In addition to the normal multiflow deionizing action an auxiliary oil-flow piston is provided to help interrupt line-charging current without delayed restriking and also to provide flushing action when interrupting high fault currents. With regard to the first function of this auxiliary piston, it is well known that successive restriking during the interruption of transmission-line-charging current or the charging current to a large capacitor installation may, under unfavorable conditions, give rise to excessive overvoltage. However circuit breakers equipped with multiflow deion grids, which without piston may show one or occasionally two restrikes, actually did not develop more than twice normal voltage on field tests.

Figure 6. Oscillogram of charging current interruption at 66 kv on 230-kv three-cycle multiflow deion grid unit

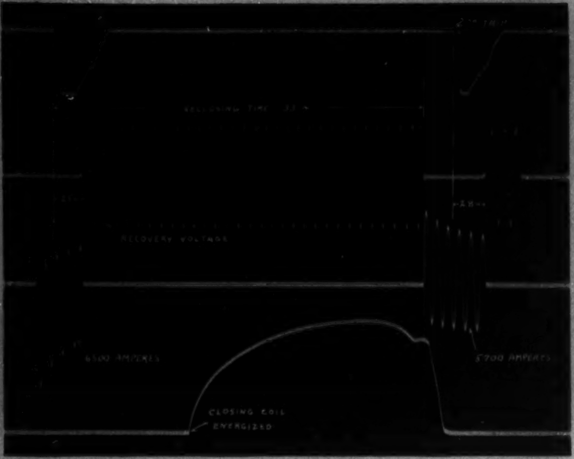


Figure 7. Oscillogram of 230-kv multiflow deion grid interruption at 132 kv to ground on 35-cycle reclosing duty

Fault equivalent to 2,500,000 kva, three phase, opened twice with interrupting time less than three cycles on each tripping operation

Transients of this magnitude may be encountered in any case during the energizing of a line or interrupting fault currents.

When only two interrupting units per pole for 230 kv were used, it was found that the contact separation at the end of one-half cycle after contact parting was insufficient to hold double normal voltage even with the gap completely filled with clean oil. The piston was, therefore, arranged with a delayed pickup so that oil flow is not started until after the contacts have opened approximately one inch. At the next current zero, the charging current is extinguished, and the insulation established in the contact gap becomes adequate to prevent restriking of the arc. Note from Figure 5 that the oil flow from the piston is directed through a single orifice in the top break to develop additional dielectric strength in this gap before passing into the channels to the multiorifice structure in the main break. In Table I are listed 18 interruptions of charging currents with 45, 90, and 180 amperes at 66,000 volts across a single grid unit, and also six interruptions of 90 amperes at 132,000 volts across two grids. No delayed restrikes occurred on any of these tests, a typical oscillogram being shown in Figure 6.

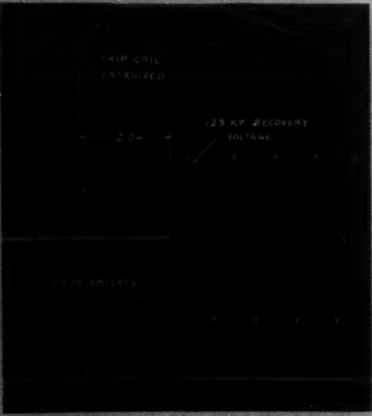


Figure 8. Oscillogram of 230-kv multiflow deion grid interruption at 132-kv-to-ground clearing fault equivalent to 4,300,000 kva, three phase, within two cycles after energizing trip coil using only two grid units per breaker pole

Table II. 230-Kv Three-Cycle Breaker Tests (Two Grids Per Pole)

(a). 132 Kv to Ground, 35-Cycle Reclosing Duty Cycle

	Interrupted Current (Amperes)		Interrupting Time (Cycles)		
Test Number	1st Interruption	2nd Interruption	1st Interruption	2nd Interruption	*Reclosing Time (Cycles)
1	4,050	4,050	2.6	2.6	33
2	3,950	3,800	2.8	2.8	32
3	4,150	4,150	2.8	2.8	33
4	6,500	5,700	2.5	2.8	33
5	6,200	5,700	2.8	2.9	33
6	6,200	5,600	2.3	2.9	34
7	3,350	2,900	2.8	3.0	33
8	3,500	3,050	2.8	3.0	33

(b). 132 Kv to Ground, Single Opening Tests

Test Number	Interrupted Current (Amperes)	Interrupting Time (Cycles)	Equivalent 230 Kv, Three-Phase Kva
1	5,300	2.35	2,100,000
2	5,600	2.3	2,220,000
3	5,300	2.5	2,100,000
4	6,200	2.2	2,450,000
5	10,900	2.0	4,300,000
6	8,800	2.4	3,500,000
198 Kv to Ground (Corresponds to Ungrounded Line Condition)			
7	4,100	2.9	1,620,000
8	4,600	3.0	1,820,000
9	6,300	2.9	2,500,000

* Time from initially energizing trip coil to reestablishment of circuit on reclosing stroke.

When short-circuit currents are interrupted, the pressure developed by the arc within the interrupting unit is greatly

Table III. 230-Kv Five-Cycle Breaker Tests

132 Kv to Ground, 35-Cycle Reclosing Duty Cycle

	Interrupted Current (Amperes)		Interrupting Time (Cycles)		
Test Number	1st Interruption	2nd Interruption	1st Interruption	2nd Interruption	*Reclosing Time (Cycles)
1	2,100	1,800	2.9	3.8	31.5
2	2,100	1,900	3.3	3.0	31.0
3	2,050	1,850	3.4	4.0	31.5
4	5,050	3,850	3.0	3.6	32.5
5	4,650	3,900	3.0	3.1	32.0
6	5,050	3,800	3.0	3.8	32.0
7	7,050	5,500	2.8	3.2	31.5
8	7,000	5,600	2.9	3.6	31.5
9	6,600	5,400	2.9	3.4	31.0

* Time from initially energizing trip coil to re-establishment of circuit on reclosing stroke.

in excess of the spring pressure provided behind the small oil-flow piston. The piston, therefore, does not function during the arc-rupturing process, but, after the circuit has been cleared and the pressure within the unit falls to a relatively low value, the piston then completes its delayed stroke, scavenging the entire arcing space in both contact breaks with a flow of clean oil. Even on a 20-cycle reclosing duty cycle there is sufficient time for this action to be completed so that when the breaker is reclosed, the deion grid is in substantially the same condition as it would be on an initial interruption.

The oscillogram shown in Figure 7 is rather remarkable in that it shows what is believed to be the first interruption of a current corresponding to a three-phase fault of 2,500,000 kva at 230 kv on a 35-cycle reclosing duty cycle, each interruption being accomplished in less than three cycles from energizing the trip coil. Of particular interest is the fact that this test was made with only two interrupting units in a 72-inch tank. This was one of a series of eight consecutive high-power reclosing tests listed in Table II. Also included in this table are nine tests showing the interrupting performance on single opening operations with 132 kv and with 198 kv across a single pole. The interruption of 10,900 amperes shown in Figure 8 is equivalent to a three-phase grounded fault of 4,300,000 kva, this tremendous power being cleared in the remarkably short time of only 2.0 cycles after energizing the trip coil. The 198-kv tests followed without examining the contacts, which were found at the end of the series to have suffered surprisingly little deterioration with considerable life still remaining.

In Figure 9 is the oscillogram of a 35-cycle-reclosing-duty-cycle test on a 230-kv breaker using a multiflow deion grid for five-cycle interrupting time. This grid shown in Figure 10a, differs from the three-cycle design in that there is no multiplying lever system for the moving contact and no auxiliary piston is provided for low current interruption or scavenging action. The excellent performance illustrated by the series of tests in Table III gives evidence that on the 35-cycle reclosing duty cycle, and, with relatively high-speed arc extinction, highly satisfactory performance is obtained.

However, for the 20-cycle reclosing duty cycle, insufficient time is available for the gas produced from the first interruption to be dissipated completely before the breaker is reclosed, and without the flushing action of the piston there would be a tendency, particularly in the higher-voltage breakers, for the arc to sparkover through the contact gap during the closing part of the stroke and impair the interrupting effectiveness of the second operation.

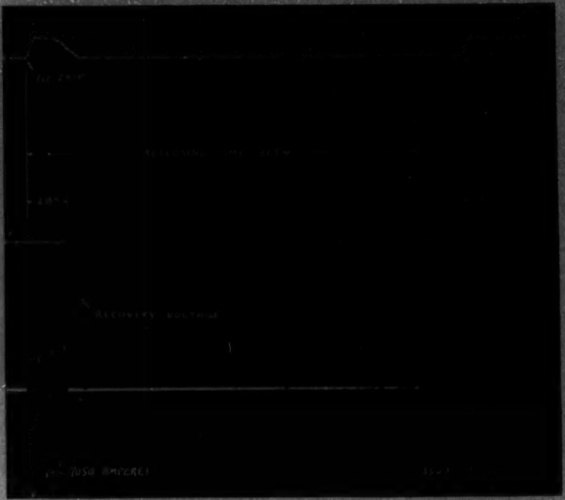


Figure 9. Oscillogram of 230-kv five-cycle multiflow-deion-grid breaker pole-unit opening fault at 132 kv to ground, equivalent to 2,800,000 kva, three-phase, on 35-cycle reclosing duty

At 138 kv the voltage gradient during a closing operation is considerably less than in a 230-kv breaker, and satisfactory reclosing performance has been obtained without a special oil-flushing arrangement. As far back as two years ago, field tests were made on a 132-kv oil circuit breaker with an older design of deion-grid contact operating on a 30-cycle reclosing duty.[3] These tests were made at currents up to 3,000 amperes or approximately 750,000 kva.

The multiflow deion grids for five-cycle service at 138 kv utilize the simplified consecutive contact-opening design illustrated by Figure 10b. A special laboratory test connection was used, supplying 44-kv three-phase power to the single-pole unit, the third phase being brought into the breaker moving contact crossbar by a flexible connection fastened to the bottom of the tank. In this way 44 kv was applied to the deion grid on each terminal, the two in series normally being subjected to 76 kv, or line-to-ground potential on a 132-kv system. The oscillogram in Figure 11 shows a short-circuit current of 9,000 amperes successfully interrupted on a 20-cycle-reclosing duty-cycle test corresponding to approximately 2,250,000 kva. The arcing time on both of these tests was approximately three cycles. No better evidence of the exceptionally low arc energy and gas generation in the multiflow deion grids under short-circuit conditions would seem to be required than the excellent results obtained on this very severe rupturing duty. However, a further striking demonstration of the low stress in the breaker tank was obtained by balancing a 1/2-inch bolt six inches long on its head on a board clamped in a

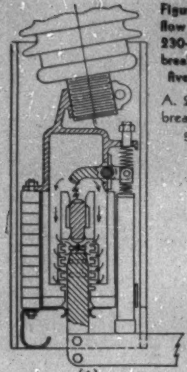
Table IV. 138-Kv Five-Cycle Breaker Tests

44 Kv Applied to Grid on Each Terminal

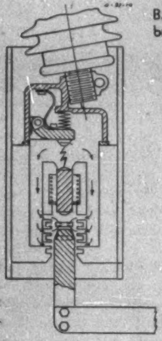
Test Number	Interrupted Current (Amperes) Grid A	Interrupted Current (Amperes) Grid B	Interrupting Time* (Cycles) Grid A	Interrupting Time* (Cycles) Grid B	Equivalent 132 Kv, Three-Phase Kva
1	5,300	3,900	4.3	4.6	1,220,000
2	5,800	4,200	4.6	4.9	1,340,000
3	5,500	7,500	4.3	4.0	1,720,000
4	6,300	7,500	4.5	4.2	1,720,000
5	5,800	7,500	4.3	4.6	1,720,000
6	8,000	8,900	4.1	4.9	2,050,000
7	9,000	9,200	4.1	4.2	2,100,000
8	9,200	8,900	4.3	4.5	2,100,000
9	10,300	11,500	4.3	4.5	2,650,000
10	9,700	11,500	4.5	4.7	2,650,000
11	18,300	15,500	4.3	4.1	4,200,000

* Based on time of 2.3 cycles from energizing trip coil to parting of contacts.

Figure 10. Multiflow deion grids for 230-kv five-cycle breaker and 138-kv five-cycle breaker


(A)

A. 230-kv five-cycle breaker, multiorifice grid assembly


(B)

B. 138-kv five-cycle breaker, multiorifice grid assembly

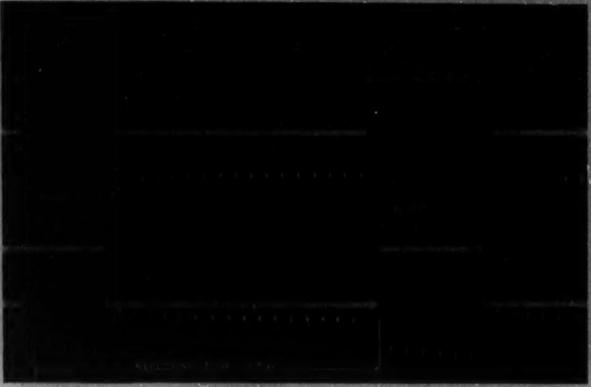


Figure 11. Oscillogram of 138-kv five-cycle multiflow-deion-grid-breaker pole-unit opening fault at 44 kv across each unit equivalent to 2,250,000 kva, three-phase, reclosing within 22 cycles for second tripping operation

level position on top of the breaker tank. Single opening tests were carried up to short-circuit currents of as high as 18,300 amperes (see Table IV) corresponding to a three-phase fault of 4,200,000 kva at 132 kv, without the steel bolt toppling over.

Conclusion

As a result of extensive design and testing of high-voltage oil circuit breakers suitable for rapid reclosing duty cycles, the following general conclusions may be drawn:

1. If the recently developed multiflow-deion-grid type of contacts is used, it appears practical to build high-voltage oil circuit breakers of maximum kilovolt-amperes with structural dimensions determined almost entirely by the electrical-insulation requirements.

2. Using a small auxiliary oil-flushing piston, a 230-kv breaker has been designed which is capable of interrupting line-charging current with a minimum of restriking.

3. The low arc energy of the multiflow interrupters makes them particularly suitable for high-capacity oil circuit breakers rated from 115 kv to 230 kv applied on rapid reclosing duty cycles.

Reference

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Reclosing of Single Tie Lines Between Systems

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THE urgent necessity for pooling reserve generating capacity has accentuated the importance of single tie-line interconnections equipped with high-speed reclosing breakers. In many cases faults on a single tie line between two sytems can be cleared, and the line can be reclosed with the same reliability that would be obtained if two lines were used without high-speed reclosure. Where a single tie line is used to transmit power between two systems the line must be reclosed fast enough, following a fault, so that synchronism is maintained. Further, the resultant power surges and voltage dips should not cause excessive shock to the system. A brief review of system performance during a reclosing cycle is presented to outline the factors that are involved.

Many practical cases have been analyzed to determine the transient-stability limits for successful reclosure of single tie lines between steam-generating stations. These studies covered a wide range of generating capacities, amount of power transfer, various transmission voltages, and distances between systems. These data are shown in curve form which should be quite useful in determining the feasibility of reclosing and as a guide to the type of commercially available equipment best suited for a specific case.

System Performance During a Reclosing Cycle

In order to visualize readily what happens during a complete reclosing cycle, it is convenient to divide the sequence of operation in four steps, as follows:

1. The initial condition (steady state prior to fault).
2. The fault condition.
3. The fault-cleared condition (breaker open or de-energized period).
4. Condition after breakers are reclosed.

The sequence of events which take place during the reclosing cycle causes the electric outputs of the generators to differ from their respective prime-mover inputs. These changes result in swings of the rotors, the magnitudes of which are determined by the specific system and machine characteristics and the operating conditions. Figures 1, 2, and 3 illustrate a typical case of successful reclosure following a fault on a single tie line. If reclosing had not been applied, in order to transmit firm power between the systems, two lines with a total of four breakers and control equipments would have been required. This example will be recognized as the familiar transient-stability problem in which the rotor positions and system voltage and current changes are obtained by well-known step-by-step stability calculations.[1,2,3]

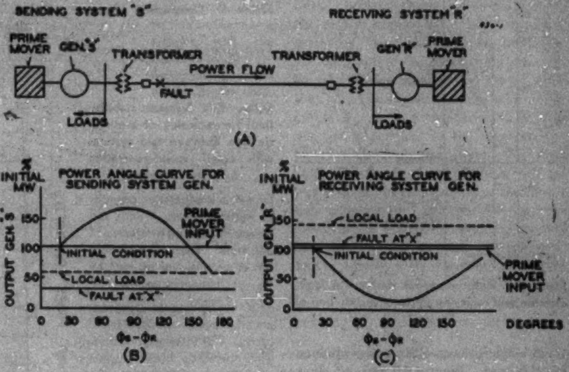

Figure 1. Power-flow characteristics of two systems connected by a single tie line

A. System diagram

B. Power-angle curves of the sending system for normal three-phase fault and line-open conditions

ϕ_S = rotor phase angle of the sending-system generators

ϕ_R = rotor phase angle of the receiving-system generators

C. Same as B except for receiving system

It is evident that, for any particular problem of tie-line reclosing involving a specific system, the factors affecting the transient-stability problem, and thus the choice of switching and control equipment, depend upon the electric-power output of the synchronous machines during the four periods in the reclosing cycle and the duration of these periods. The power relations can be obtained either analytically or preferably by a-c network-calculator solution of the electrical networks involved.

Gain in System Performance With Single-Pole Switching

Consideration of single-pole switching presupposes that the transformer banks at both ends of the tie line are grounded solidly or through low values of impedance. The protective and reclosing relays are arranged to trip and reclose only those poles necessary to clear the fault. The unfaulted wire or wires and ground are available for transmitting power and maintaining voltage during the period when the faulted wire or wires are de-energized. Since the majority of faults do not involve all three wires, single-pole switching in many cases will afford improved system performance.

Figure 4 shows a comparison of the phase-angle swings for three single-pole (individually operated) versus three-pole (gang-operated) breakers for various types of faults in a typical system. For a three-phase fault (Figure 4*A*) all three poles have to be opened to clear the fault so that there is no comparison between

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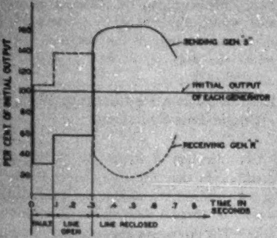


Figure 2. Electric-power output of generators for a typical reclosing cycle of the tie line of Figure 1

The fault near the sending end of the line is cleared in 0.1 second, and the line reclosed in 0.3 second

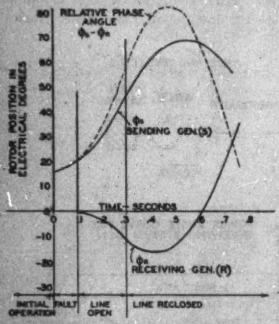


Figure 3. Rotor positions during the fault-clearing and reclosing cycle of the tie line of Figure 1

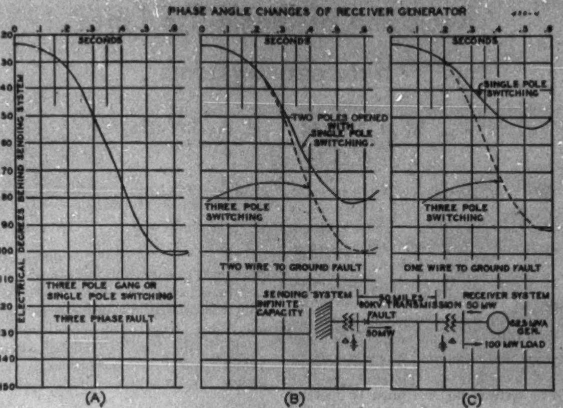


Figure 4. Comparison of system swings following fault, clearing of fault, and reclosing of a single tie line

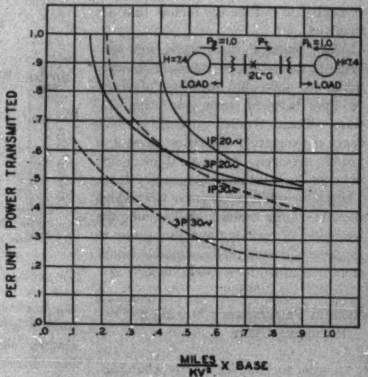
Fault cleared in nine cycles (0.15 second), and the line reclosed in a total time of 21 cycles (0.35 second)

Figure 5. Transient-stability limit for reclosing of a single tie line between two systems with equal steam-generating capacity connected

Base equals generating capacity in either system. Per unit power transmitted is referred to this base. Faults are double line to ground cleared in 0.15 second (nine cycles)

3P30~ = three-pole gang-operated breakers reclosed in 30 cycles. 3P20~ = three-pole gang-operated breakers reclosed in 20 cycles

1P30~ = individual pole-operated breakers reclosed in 30 cycles. 1P20~ = individual pole-operated breakers reclosed in 20 cycles



three single-pole or three-pole switching for this case. Figures 4*B* and 4*C* show considerable improvement in system operation with single-pole switching, as indicated by the smaller swings in phase angle of the generators.

Another measure of the severity of the disturbance to the system is the drop in voltage on the load busses. In general, the voltages decrease with the increase of the phase-angle difference between the two systems. For the case of Figure 4 the voltage at maximum swing at the receiver load bus in per cent of normal is shown in Table I.

Determination of the Type of Switching to Be Used for Specific Applications

The question to be decided in the determination of the proper choice of reclosing equipment is what type of commercially available reclosing breakers and control should be used when a desired amount of power is to be transmitted a given distance over a single line between two systems. Figures 5, 6, and 7 will be found directly useful in answering this question in a great many cases. These curves present the results of investigations of over 100 practical solutions. Each curve shows the transient-stability limits based on two line-to-ground faults for various ratios of power transmitted to size of the system plotted against a measure of the electrical length of the transmission line. There are four curves in each figure, one for each of four commercially available reclosing-breaker schemes. In addition on Figure 7 there is shown the curve for three-pole 35-cycle reclosing. It is recognized that, at some of the higher-transmission voltages, this rather than 30 cycles is standard. The areas to the left and below these curves are regions below the transient-stability limit where successful reclosure would be obtained. The areas to the right and above the curves are above the transient-stability limits for the respective types of reclosing equipment.

The use of these curves is illustrated in Table II where several typical examples are shown. The choice of which group of curves to use depends on the relative sizes of the sending and receiver systems.

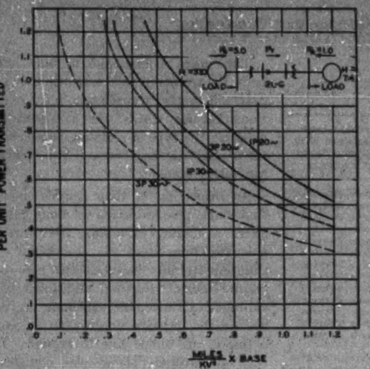


Figure 6. Transient-stability limit for reclosing of a single tie line between two systems with the steam-generating capacity of the sending system five times that of the receiving system

Base equals generating capacity in receiver system. Per unit power is referred to this base. Faults are double line to ground cleared in 0.15 second (nine cycles)

Table I

Type of Fault	Per Cent of Normal System Voltage Three-Pole Switching	 Single-Pole Switching
Three phase	73.5	73.5 (all 3 poles open)
Two lines to ground	74.5	82 (2 poles open)
One line to ground	78.0	93 (1 pole open)

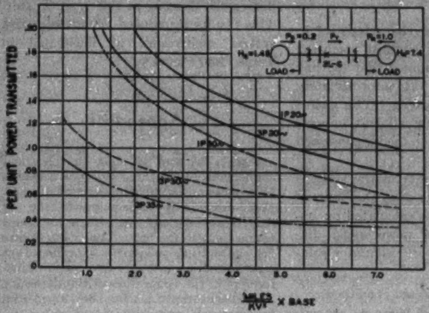


Figure 7. Transient-stability limit for reclosing of a single tie line between two systems with the steam-generating capacity of the receiving system five times that of the sending system

Base equals generator capacity in receiving system. Per unit power transmitted is referred to this base. Faults are double line to ground cleared in 0.15 second (nine cycles)

Figure 5 is used if they are equal substantially. Figure 6 is used if the sending-end connected capacity is in the order of five times that of the receiver. Figure 7 is used if the receiver system has five times the capacity of the sending system. For other relative sizes interpolation is possible.

The data plotted on Figures 5, 6, and 7 were obtained from transient-stability solutions of examples with constants and initial operating conditions so chosen that the results applied to a large range of systems. The initial operating conditions for each point on the curves were obtained by establishing voltage and power flows that could be expected reasonably under practical operating conditions. The base selected was equal to the kilowatt capacity of the receiver system. The total amount of generation in either system was assumed to be made up of the necessary number of 1,800-rpm 80 per cent power-factor machines operating at full kilowatt load and excitation as required. The constants of the machines used were $H=7.4$, $x_d'=0.23$, $x_2=0.15$, $r_1=0.900$, and $r_2=0.035$. The shunt loads were assumed to be at 85 per cent power

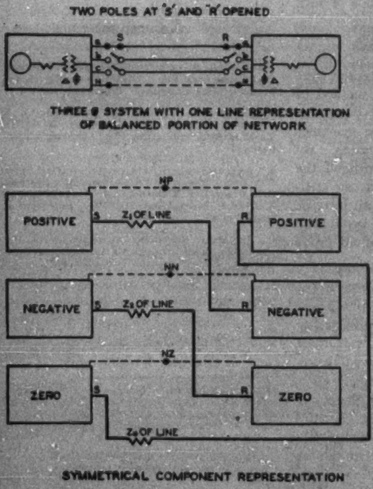

TWO POLES AT 'S' AND 'R' OPENED
S
R
a
b
c
N
THREE ϕ SYSTEM WITH ONE LINE REPRESENTATION OF BALANCED PORTION OF NETWORK
NP
NN
NZ
POSITIVE
NEGATIVE
ZERO
S
R
Z₁ OF LINE
Z₂ OF LINE
Z₀ OF LINE
SYMMETRICAL COMPONENT REPRESENTATION

Figure 8. Electrical representation of condition following the clearing of a fault on a single tie line

A (left). Two-line-to ground fault on a short line (negligible charging capacity)

B (right). Two-line-to ground fault on a long line (charging capacity included)

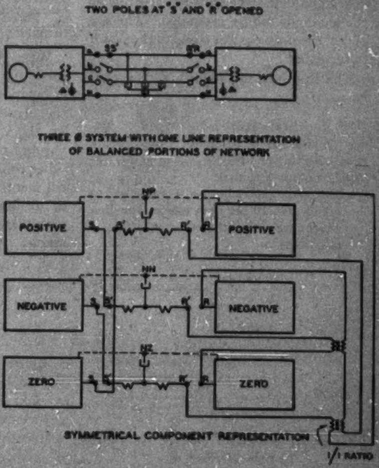

TWO POLES AT 'S' AND 'R' OPENED
S'
R'
a
b
c
N
THREE ϕ SYSTEM WITH ONE LINE REPRESENTATION OF BALANCED PORTIONS OF NETWORK
NP
NN
NZ
POSITIVE
NEGATIVE
ZERO
S
S'
R'
R
SYMMETRICAL COMPONENT REPRESENTATION
1/1 RATIO

Table II. Examples of Use of Figures 5, 6, and 7 to Determine Suitable Switching for Specific Cases

PS = Generation at Sending End (Megawatts)
PR = Generation at Receiving End (Megawatts)
PT = Maximum Power to Be Transmitted (Megawatts)

	Known System Characteristics									
Case Number	PS	PR	PT	Kilovolts	Miles	Per Unit PT	$\frac{\text{Miles}}{\text{Kv}^2}\times$ Base	PS/PR	Use Figure	Synchronism* Maintained With Following Types of Reclosing
1	50	50	25	69	40	0.5	0.42	1	5	1 pole, 30 cycle 3 pole, 20 cycle 1 pole, 20 cycle
2	100	100	60	110	100	0.6	0.83	1	5	None
3	250	50	25	69	40	0.5	0.42	5	6	3 pole, 30 cycle 1 pole, 30 cycle 3 pole, 20 cycle 1 pole, 20 cycle
4	500	100	70	110	100	0.7	0.83	5	6	1 pole, 20 cycle
5	20	100	15	69	50	0.15	1.05	0.2	7	1 pole, 30 cycle 3 pole, 20 cycle 1 pole, 20 cycle
6	100	500	70	110	90	0.14	3.72	0.2	7	1 pole, 20 cycle

* When more than one type of reclosing is suitable so far as maintaining synchronism is concerned, the final choice will depend on other factors, such as the severity of the disturbance as measured by the voltage dips and power surges.

factor. The line regulation was adjusted to ten per cent. In certain cases this required that reactive kilovolt-amperes be furnished by the receiver system in excess of the capacity of the receiver generators. In these cases the additional reactive kilovolt-amperes required were assumed to be supplied by synchronous condensers. The inertia of these condensers was not included in the swing calculations as it usually was so small as to be negligible, and, even where condensers of considerable size were used, the omission of their inertia would tend to give pessimistic results.

Standard step-up and step-down transformers with per unit reactance of 0.085 were used. They were assumed to be connected delta-wye with the neutrals on the high side solidly grounded. The size of the transformers was equal to the power transmitted.

The line had the following characteristics:

x_1 = 0.71 ohm per mile
y_1 = 5.8$(10)^{-6}$ mhos per mile
r_0 = 0.45 ohm per mile
x_0 = 2.84 ohms per mile
r_1 = 0.35 ohm per mile, or a value which would give 10 per cent loss in the line, whichever gave the smaller value of resistance

In using these line characteristics to obtain systems with practical operating conditions, it was assumed that the line would be operated at approximately unity power-factor loading. This means that the line charging kilovolt-amperes would be about equal to the i^2x consumed in the line.

The transient stability of the systems for the different distances and amounts of power transferred was calculated by obtaining the required power-angle diagrams from a-c network-calculator solution for:

1. The initial and reclosed conditions.
2. Two-wire-to-ground faults as the criterion for transient stability.
3. Two poles open for single-pole-opening breakers (see appendix).
4. Three poles open for gang-operated breakers.

When these power-angle diagrams were used, the usual step-by-step method of calculation was employed to determine the phase-angle swings of the machines for the various types of operation. By varying the length of line with all the other conditions held constant, the critical point was reached where an increase in distance would result in instability, and where shorter distances would prove stable.

Conclusion

The curves on Figures 5, 6, and 7 embody the results of such a multiplicity of practical system transient-stability solutions that they can be used as a guide in choosing the type of reclosing equipment for a single tie line between two steam-generating systems.

An inspection of these curves shows that single-pole switching in many cases offers a marked advantage over three-pole switching from the standpoint of transient stability based on two wire-to-ground faults. Further, additional advantages of single-pole switching accrue because the majority of faults (70 per cent or more) are single line to ground. If only one breaker pole is open, considerable synchronizing power is maintained during the clearing of this type of fault. This results in reduced voltage dips and system oscillations, as illustrated in Table I and Figure 4.

Appendix I

Figures 8A and 8B show the electrical connections of the three sequence networks to represent the unbalanced system conditions obtained by single-pole switching. It may be noted that, where there are shunt connections to the opened line as in Figure 8B, the multiple unbalances so produced are represented by using 1-to-1 ratio transformers in the connection of the networks. These connections have been developed from fundamentals previously described.[4]

References

1. Electrical Transmission and Distribution Reference Book, Westinghouse Electric and Manufacturing Company. Chapters 8 and 9.

2. High-Speed Single-Pole Reclosing, J. J. Trainor, J. E. Hobson, H. N. Muller, Jr. AIEE Transactions, volume 61, 1942, February section, pages 81–7.

3. Analysis of the Application of High-Speed Reclosing Breakers to Transmission Systems, S. B. Crary, L. F. Kennedy, C. A. Woodrow. AIEE Transactions, volume 61, 1942, June section, pages 339–48.

4. Sequence Network Connections for Unbalanced Load and Fault Conditions, E. L. Harder. *Electric Journal*, December 1937.

Rectifier-Circuit Duty

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MEMBER AIEE

THE usual rectifier unit consists of a transformer with a number of rectifying elements connected to its secondary windings. The rectifying action may be resolved into two parts, namely, the circuit action which is determined by the transformer and the a-c and d-c circuits, and the unidirectional switching action which is performed by the rectifying elements. The circuit action is defined by the number, arrangement, and constants of the circuits connecting the a-c and d-c systems. The switching action consists of connecting the various secondary windings of the transformer to the load circuit at selected intervals during each cycle by means of mercury-arc rectifiers, metallic rectifiers, vacuum tubes, and other types of rectifying elements.

The size and characteristics of the rectifying elements required for any rectifier unit depend upon the current each must carry during the conduction period and the voltage it must withstand during the idle period. Although the rectifying element may control the time at which conduction starts (as, for example, in phase-controlled rectifiers), in other respects it is a passive element, since the voltage and current impressed upon it are determined by the circuit constants. The nature and value of the voltage and current appearing on the rectifying elements, that is, the loading on these elements, may be termed the circuit duty. Conversely, the capability of the rectifying element may be termed the rectifier capacity. The rectifier capacity may be measured in terms of the maximum circuit duty at which it is capable of operating without failure.

These terms may be defined as follows:

1. **The rectifier-circuit duty is the voltage and current loading applied to the rectifying element by the rectifier circuit.**
2. **The rectifier capacity is the maximum loading at which the rectifying element is capable of operating without failure. It may be expressed in the same terms as the circuit duty.**

The concept of rectifier-circuit duty and rectifier capacity provides a convenient and useful means for analyzing the action taking place in any rectifier equipment. It permits a division of engineering functions into two parts, one of which deals with the analysis and application of rectifier circuits, whereas the other is concerned with the design and construction of suitable rectifying elements. Such a division of the engineering functions involved in rectifier design is not new but has been employed for some time in the design of small vacuum-tube rectifiers, where the performance of the tube is specified by itself, and the same tube may be used in a wide variety of applications. The effect of circuit upon performance is recognized also in the design and application of metallic rectifiers.

The application of this method of analyzing rectifier action requires a knowledge of the factors which limit the rating of the rectifying elements. Since arc-back behavior is a primary consideration in the performance and rating of mercury-arc rectifiers, and the functional relations between circuit factors and arc-back have not been established fully, this method has heretofore been used only in a very general way. However, a better understanding of the factors which limit rectifier rating is obtained if the circuit duty is determined on the basis of relations between the impressed voltage and current and the occurrence of arc-back, as indicated by studies of the physical action taking place within the rectifier.

It is the object of this paper to present an analysis of rectifier circuits in terms of circuit duty and rectifier capacity, discuss the various factors involved, and compare the various types of rectifier circuits.

Elements of Circuit Duty

It appears axiomatic that the duty on the rectifying elements arises from both the magnitude and the shape of the voltage and current waves, the effect of wave form depending upon the type of rectifying element employed.

Typical voltage and current waves on the rectifying element in a rectifier circuit are shown in Figure 1. A study of the physical processes which take place in the rectifying elements indicates that the circuit duty may be expressed in terms of the following voltages and currents:

Voltage

1. **Peak inverse voltage.**
2. **Rms inverse voltage.**
3. **Initial inverse voltage.**
4. **Peak forward voltage.**

Current

5. **Average current.**
6. **Peak current.**
7. **Rms current.**
8. **Final commutation rate.**

PEAK INVERSE VOLTAGE

The peak inverse voltage is a measure of the maximum dielectric stress applied across the rectifying element during the idle period. Failure from excess voltage may take various forms, such as flashover, puncture of dielectric materials, or excessive glow discharge resulting in arc-back.

RMS INVERSE VOLTAGE

The rms value of the inverse voltage appears to be important only in the case of rectifiers having appreciable leakage current during the inverse period, where it provides a measure of the heating resulting from this leakage current. The leakage resistance characteristics of metallic rectifiers (such as the copper-oxide and selenium types) are nonlinear, and an accurate determination of the losses may require weighting factors other than those given by rms values. However, the heating due to the leakage current is usually given in terms of the rms values of the inverse voltage because of the ease with which such values may be determined.

INITIAL INVERSE VOLTAGE

The ionization in a mercury-arc rectifier does not disappear immediately upon the extinction of anode current, and, therefore, some residual ionization is present during the idle period. The amount of the residual ionization is greatest at the beginning of the idle period and decays as a function of time. The action of the

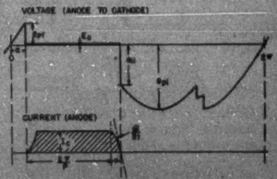

Figure 1. Typical voltage and current waves on rectifying element

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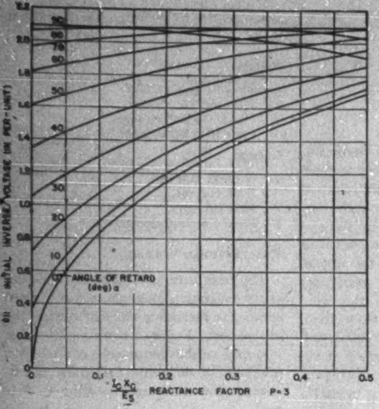


Figure 2. Initial inverse voltage $P=3$

Per unit base is E_{do}

$$\frac{e_{ii}}{E_{do}}=\frac{2\pi}{P}\sin(u+\alpha)$$

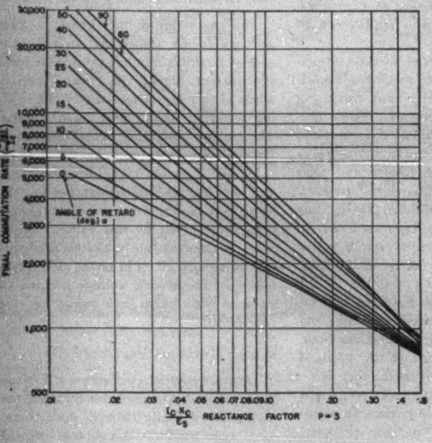


Figure 3. Final commutation rate $P=3$, $f=60$ cycles

Multiply values of $\frac{di}{dt}/I_c$ given on curve by direct current commutated I_c to obtain commutation rate in amperes per second

$$\frac{di}{dt}=\left[\frac{\omega}{\sqrt{2}}\frac{P}{\pi}\sin\frac{\pi}{P}\right]\cdot\frac{E_s}{X_c}\cdot\frac{e_{ii}}{E_{do}}$$

rectifier circuit results in a sudden application of negative voltage across the rectifying element immediately following conduction. This negative voltage acting upon the residual ionization attracts positive ions to the anode. Since the positive ion bombardment may cause arc-back, the initial inverse voltage is a factor in the duty applied to a mercury-arc rectifier.

Peak Forward Voltage

When a rectifier is operated with the d-c voltage reduced by phase control, a positive voltage is applied across the rectifying element during the latter part of the idle period. This forward voltage imposes a further requirement upon the control characteristics of the rectifying element, since conduction must not occur before the desired firing time.

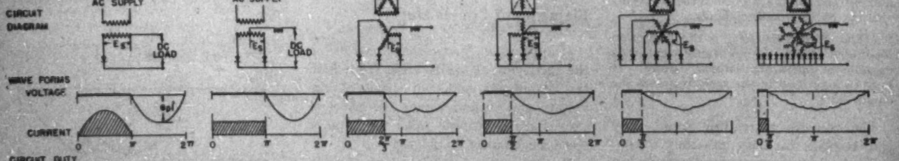
Average Current

The average current passed by the rectifying element is a convenient measure of the duty imposed upon it, since this value bears a simple relation to the load current on the rectifier unit. For this reason the current capacity of the rectifying element is customarily expressed in terms of average current. However, the average current does not bear so definite a relationship to the physical action and physical limitations of the rectifying element as do the peak and rms current values.

Peak Current

The losses resulting from the passage of current are one of the factors which limit the capacity of the rectifying elements. However, the nature of the current limitations in mercury-arc rectifiers is somewhat different from that in other electric apparatus, since failure by arc-back may occur without measurable temperature

Figure 4. Comparison of circuit duty of simple rectifiers



NO. OF PHASES	1.	2	3	4	6	12
NAME	SINGLE PHASE	BI PHASE	THREE PHASE	QUARTER PHASE	SIX PHASE	TWELVE PHASE
CIRCUIT DUTY						
DC VOLTAGE E_{do}/E_s	= 0.45	0.90	1.17	1.27	1.35	1.41
PEAK INVERSE VOLTAGE e_{pi}/E_{do}	= 3.14	3.14	2.09	2.22	2.09	2.02
AVG. ANODE CURRENT*	= 1.00	0.50	0.33	0.25	0.167	0.083
RMS ANODE CURRENT*	= 1.57	0.707	0.577	0.50	0.408	0.288
PEAK ANODE CURRENT*	= 3.14	1.00	1.00	1.00	1.00	1.00

* (IN PER-UNIT ON BASE OF LOAD CURRENT I_d)

rise. As the instantaneous arc drop is a function of the instantaneous current, the peak anode current is an important factor in the determination of the loading. Furthermore, the conditions existing in the mercury vapor at the end of the conducting period are a function of the peak current which has passed during conduction, and the peak current, therefore, influences the probability of arc-back. In the case of heated cathode rectifiers, the output of the rectifier is limited by the maximum emission which may be obtained from the cathode without damage.

Rms Current

The heating in the leads and other conducting parts of the rectifying element depends upon the rms value of the current.

Final Commutation Rate

The amount of residual ionization present at the beginning of the inverse period depends in large measure upon the rate of reduction of current during the last few degrees of conduction. The final commutation rate is the rate of change of anode current at the instant of extinction and in some cases may be taken as a measure of the amount of residual ionization present in the rectifier at the time that the initial inverse voltage appears across it. Therefore, the final commutation rate is a further element in the duty on the rectifier.

The commutation rate also may be of importance during the initial part of the conduction period when the rate of current build-up exceeds the ionization rate of the rectifier. Under such conditions additional local heating and voltage stresses may be imposed upon the rectifier.

The researches of Kingdon and Lawton[1] have shown that there is a definite relation between the residual ionization and the arc-back frequency. Under certain conditions the frequency of arc-back due to residual ionization is a function of the product of the initial inverse voltage by the final commutation rate. Since arc-backs in mercury-arc rectifiers are caused by many factors, this relationship has not been established fully on commercial rectifiers. However, experience with many rectifier installations indicates that some such relationship does exist.

Determination of Circuit Duty (Circuit Analysis)

The circuit duty may be determined by an analysis of the action in a simple rectifier circuit. The formulas for the various elements of circuit duty for a simple rectifier of P phases follow.

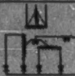
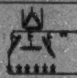
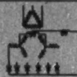
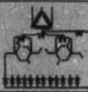
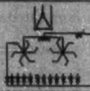
NO. OF PHASES IN EACH SIMPLE RECTIFIER p	NO. OF PHASES IN EQUIV. SIMPLE RECTIFIER P	NO. OF SIMPLE RECTIFIERS n	CIRCUIT DIAGRAMS
2 (DOUBLE-BIPHASE)	4	2	
2 (TRIPLE-BIPHASE)	6	3	
3 (DOUBLE-WYE)	6	2	
3 (QUADRUPLE-ZIG-ZAG)	12	4	
6 (DOUBLE-SIX PHASE)	12	2	

Figure 5. Multiple rectifiers

The peak inverse voltage (e_{pi}) expressed in terms of the theoretical d-c voltage E_{do} is

$$e_{pi}=K\frac{\pi}{P\sin\frac{\pi}{P}}E_{do} \qquad (1)$$

where

K = ratio of secondary voltage across phases to secondary voltage line to neutral (When $P=3$ the value of K is $\sqrt{3}$, and when $P=2, 4, 6$, and 12 the value of K is 2)

The rms inverse voltage is not given here but may be determined from the inverse-voltage wave by integration.

The initial inverse voltage (e_{ii}) is given by the expression

$$e_{ii}=\frac{2\pi}{P}E_{do}\sin(u+\alpha) \qquad (2)$$

where

u = angle of overlap and
α = angle of phase retard

The initial inverse voltage for a simple three-phase rectifier is shown in Figure 2.

The peak forward voltage (e_{pf}) is

$$e_{pf}=\frac{2\pi}{P}E_{do}\sin\alpha \qquad (3)$$

The average and rms current passing through the rectifier elements may be expressed in terms of the direct current commutated I_c (peak current) as follows:

$$\text{average current}=\frac{I_c}{P} \qquad (4)$$

$$\text{rms current}=\frac{I_c}{\sqrt{P}} \qquad (5)$$

The final commutation rate (di/dt) may be obtained by differentiating the expression for the commutating current. The resulting value is

$$\frac{di}{dt}=\left[\sqrt{2}\,\omega\sin\frac{\pi}{P}\right]\cdot\frac{E_s}{X_c}\cdot\sin(u+\alpha) \qquad (6)$$

$$=\left[\frac{\omega}{\sqrt{2}}\,\frac{P}{\pi}\sin\frac{\pi}{P}\right]\cdot\frac{E_s}{X_c}\cdot\frac{e_{ii}}{E_{do}} \qquad (7)$$

where

$\omega=2\pi f$, f = frequency of supply system
X_c = commutating reactance, ohms line to neutral
E_s = secondary voltage to neutral in rms volts

The variation in final commutation rate with the reactance factor is shown in Figure 3.

The analysis of any of the more complex rectifier circuits is facilitated by breaking them down into their component simple rectifiers. In cases where the rectifier installation has many phases which are displaced so as to obtain an equivalent simple rectifier with many phases, commutations will overlap, and the determination of the initial inverse voltage and final commutation rate becomes more difficult. Although it is possible to analyze such circuits, in general, it may be more practical to determine these elements of circuit duty by direct measurement.

Circuit Constants Versus Circuit Duty

Rectifier specifications covering rating and operating characteristics usually include the following factors:

1. Connection diagram (number of phases of equivalent simple rectifier).
2. D-c voltage.
3. Load current.
4. Voltage regulation (reactance of transformer and a-c system).
5. Amount of phase control.

The preceding factors, while they are very useful in the description of the complete equipment, do not provide much information with regard to the choice of rectifying elements. However, these factors bear a functional relationship to the elements of circuit duty and serve

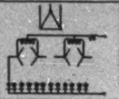
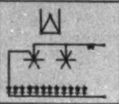
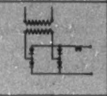
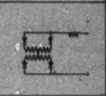
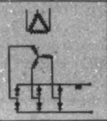
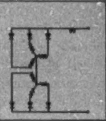
NO. OF PHASES IN EACH SIMPLE RECTIFIER P	NO. OF PHASES IN EQUIV. SIMPLE RECTIFIER q	NO. OF SIMPLE RECTIFIERS R	CIRCUIT DIAGRAMS
3 (QUADRUPLE-WYE)	6	4	
6 (DOUBLE-SIX PHASE)	6	2	

Figure 6. Parallel rectifiers

Figure 7. Double-way rectifiers

NO. OF PHASES IN EACH SIMPLE RECTIFIER P	NO. OF PHASES IN EQUIV. SIMPLE RECTIFIER q	NO. OF SIMPLE RECTIFIERS R	CIRCUIT DIAGRAMS	EQUIVALENT SEC. CIRCUIT
2	2	2		
3	6	2		

as a basis for the determination of the circuit duty imposed on the rectifying elements. The relationship between these factors and circuit duty is shown by the following examples:

Simple Rectifiers

The wave form of the voltage and current on the rectifying elements for various simple rectifiers is shown in Figure 4. The circuit duty for each of these connections may be calculated by the foregoing formulas.

Multiple Rectifiers

Most rectifier installations use a combination of simple rectifiers in multiple, parallel, cascade, or series in order to obtain the most economical arrangement and the desired operating characteristics. Such circuits may be broken down into component simple rectifiers and circuit duty determined from the foregoing formulas. A number of commonly used multiple rectifier circuits is shown in Figure 5 and two parallel rectifier circuits in Figure 6.

Double-Way Rectifiers

Double-way circuits may be reduced to the form of simple rectifiers in cascade or series. Figure 7 shows two frequently used double-way rectifier circuits with the equivalent cascade or series circuits.

Comparison of Three Six-Phase Circuits

A comparison of the circuit duty incurred in three different six-phase rectifier circuits having the same rating and the same regulation characteristics is shown in Figure 8. The values given in this comparison are based on a six-anode rectifier rated 1,500 kw at 600 volts and 2,500 amperes. The theoretical voltage for each of the three cases is $E_{d0}=650$, and the voltage regulation due to transformer reactance is assumed to be $E_x/E_{d0}=0.035$. With the usual transformer resistance such a rectifier would have a normal regulation of approximately five per cent. It is assumed that the rectifier is operated with no phase retard.

Effect of Reactance

The effect of reactance upon the initial inverse voltage and final commutation rate is shown in Figure 9. With reference to this figure, the solid curves are the same as those for the delta double-wye rectifier circuit in Figure 8 and for a rectifier having a voltage regulation $E_x/E_{d0}=0.035$. The dotted curves give the values for a rectifier having twice the transformer reactance and, therefore, twice the value of regulation. The effect of increased reactance is to increase the initial inverse voltage and to reduce the commutation rate.

Effect of Phase Control

The effect of phase control upon the initial inverse voltage and final commutation rate is shown in Figures 2 and 3. The application of phase control increases both the initial inverse voltage and the final commutation rate. Experience shows that the probability of arc-back is increased when a rectifier is operated with phase control.

Selection of Rectifier Circuits

The choice of the rectifier circuit for any specific application is based on considerations involving wave form, power factor, regulation, and efficiency requirements. The rating and characteristics of the rectifying elements available must be considered also.

Wave Form

The number of phases of the rectifier circuit is selected so as to obtain a suitable wave form. Most rectifier equipments in the higher ratings consist of a number of rectifier units displaced in phase so as to reduce the magnitude of the harmonics in the a-c and d-c systems. The minimum number of phases is usually six, and the number increases to 12, 24, 36, and so forth for the larger installations.

Regulation

The inherent regulation of the rectifier is determined primarily by the reactance of the rectifier transformer. The value of reactance is chosen so as to obtain the desired regulation characteristic.

Another important consideration in the selection of transformer circuit reactance is the magnitude of fault current under d-c short circuit and arc-back. The fault current is a primary factor in the circuit duty under short-circuit conditions, and the circuit reactance must limit the current to a value less than the fault-current capacity of the rectifying elements.

The effect of type of circuit upon the transformer reactance and d-c short-circuit current is shown in the comparison of three six-phase circuits in Figure 8.

In 12-phase circuits the transformer reactance and the d-c short-circuit current depend upon the arrangement of the transformer windings. For example, by interlacing the secondary windings of the quadruple zigzag transformer shown in Figure 10*b*, the values of transformer reactance will be twice and of d-c short-circuit current half those obtained on the quadruple-wye transformer shown in Figure 10*a* for the same voltage regulation.

One advantage of the double-way circuit shown in Figure 7 is that there is no d-c feedback during arc-back, since this circuit is equivalent to two simple rectifiers in series.

Efficiency

Best utilization of transformer windings is obtained usually with a circuit consisting of a number of simple three-

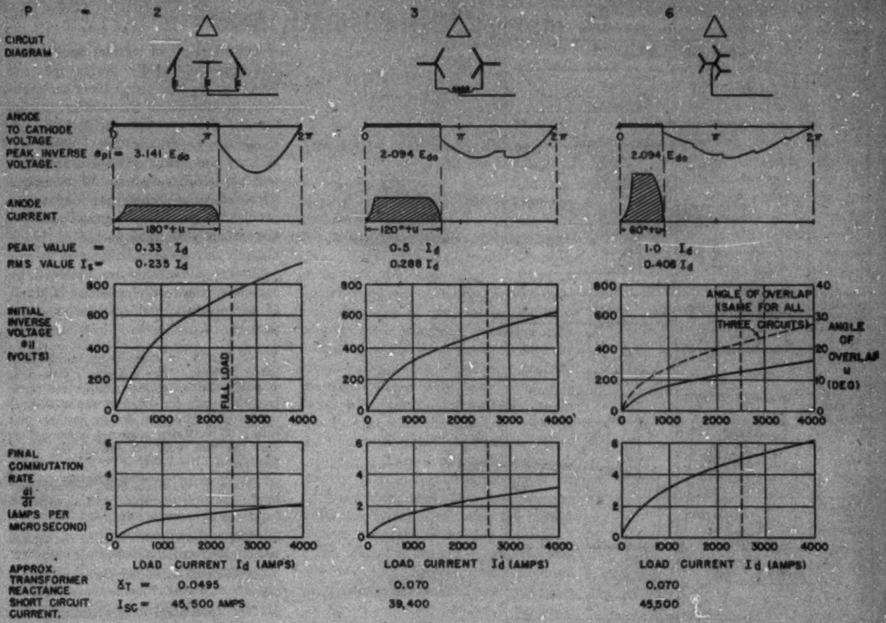


Figure 8. Comparison of three six-phase circuits for rectifier rated 600 volts, 1,500 kw

phase rectifiers arranged in multiple and parallel combinations, and, for this reason most rectifier installations employ this type of circuit. These circuits also result in a good utilization on the rectifier.

A six-phase fork connection is used often on certain sizes of rectifiers. Some of its advantages are elimination of the interphase transformer, simplification of the control circuit, and elimination of the possibility of transformer unbalance. However, with this circuit, the peak anode current has twice the value obtained in a double-wye circuit, and the rectifier capacity is reduced, while the arc losses are increased. Tests on six-anode ignitron rectifiers show that the full-load arc losses are approximately 8 per cent greater when operating fork-connected than when operating double-wye.

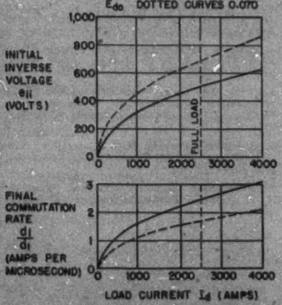


Figure 9. Effect of reactance on initial inverse voltage and final commutation rate

Special Circuits

A number of circuits providing a variety of operating characteristics and different modes of operation is shown in Figure 11.

The first of these is a single-phase rectifier circuit having three different modes of operation. In this circuit the mode of operation is determined by the constants of the d-c load. The voltage and current wave forms for the different cases are shown.[2]

The second circuit is a simple biphase

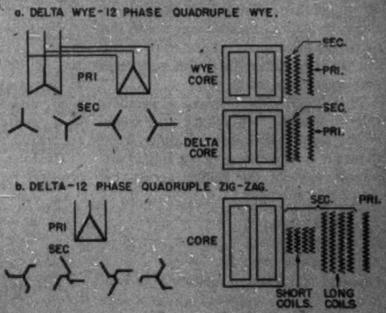

a. DELTA WYE-12 PHASE QUADRUPLE WYE.
PRI
SEC
WYE CORE
SEC.
PRI.
DELTA CORE
b. DELTA-12 PHASE QUADRUPLE ZIG-ZAG.
PRI
SEC
CORE
SEC.
PRI.
SHORT COILS.
LONG COILS

Figure 10. Quadruple-wye and quadruple-zigzag circuits

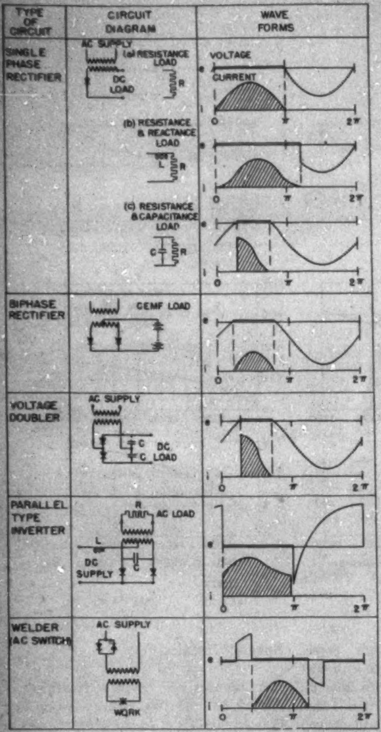


Figure 11. Special circuits

rectifier operating with a counter electromotive-force load.[3] In the usual mode of operation for this circuit the load current is intermittent. The range of intermittent operation depends on the circuit constants and the amount of phase control.

The third circuit shows a voltage doubler.[4] This is a double-way circuit, and the voltage and current waves across the rectifying element are similar to those for the single-phase rectifier with resistance and capacitance load.

The fourth circuit shows a parallel-type inverter.[5] The voltage and waves of the parallel-type inverter differ from those for most rectifiers in one important respect, namely, the voltage across the rectifying element is negative for only a short part of the inverse period immediately following conduction. During the remainder of the conduction period a positive voltage is applied across the rectifying element. This type of voltage wave is common to most inverter circuits and imposes a duty which differs from that of the usual rectifier.

The fifth circuit shows a welder or a-c switch.[6] This type of circuit employs two rectifying elements connected so as to permit current flow in opposite directions, and the combination operates as an a-c switch. It has found widespread application in resistance welding.

The foregoing examples are only a few of a large variety of new applications.

Conclusion

The diversity of rectifier applications has increased rapidly during the past few years, so that today there are available, in addition to the rectifier, a large number of power electronic devices, such as rectifiers with voltage control, inverters, frequency changers, welders, and electronic switches. With such a wide variety of rectifier applications the problem of matching the performance of the circuit with that of the rectifying element assumes greater importance.

Several advantages are obtained by expressing the rectifying action in terms of circuit duty and rectifier capacity, namely:

1. A division of engineering functions is made possible. The analysis, calculation, and selection of rectifier circuits and rectifying elements for different applications is one project while the design, testing, and rating of rectifying elements is the other; and the two may be carried out independently.
2. The elements which must be considered in determining the capacity of all types of rectifying elements are defined.
3. The method for determining the limitations of the rectifying elements is indicated.
4. A common basis for the rating of all types of rectifying elements is provided.
5. The application of any rectifying element to a wide variety of circuits is facilitated.

It is hoped that this method of analysis may be extended to all types of circuits using rectifiers, and that a standard basis for rating all types of rectifying elements may be developed.

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A Versatile Power-Line-Carrier System

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POWER-LINE carrier has kept pace with the swift progress of the electronic art during the last several years. This progress has been due mainly to the stimulus injected by the war effort. In most of the specialized fields of electronics, the advancement was necessitated and promoted strictly for military purposes. Power-line carrier, however, was and is the soldier of the home front. Its duties have been expanded to include as many functions as possible consistent with reliability, economy, and convenience. The economic feature is especially significant now, since, during wartime emergencies, the material resources of the nation are allotted, and properly so, to those agencies concerned with the battle front. Material for new power lines, new generating equipment, new telephone lines, and for other similar programs of expansion is therefore not generally available. However, power-line carrier comes to the rescue and effectively provides additional circuits and higher power-handling capacity for existing installations.

Functions of Power-Line Carrier

For over two decades carrier frequencies have been used on power transmission lines to provide a channel between generating plants, substations, and similar terminals for communication, and, more recently, for high-speed relaying. However, with the development and expansion of large interconnected power systems, the available carrier channel has been used for a number of other functions. In fact, the additional services provided by a carrier system often have been the deciding factor in justifying its installation. A typical list of functions would include the following:

1. Carrier relaying (and emergency communication).
2. Telemetering and load control.
3. Supervisory control.
4. Remote or transferred tripping.
5. Communication.

It is the purpose of this paper to show how a carrier channel can be utilized to provide these additional functions by means of audio tone modulation of the carrier signal.

Advantages of Tone Modulation

A power-line-carrier installation employing unmodulated carrier can be used for only one function at a time, such as relaying, telemetering, or communication. By modulating the carrier with audio tones and separating the tones at the receiving end of the circuit with suitable filters, it is possible to maintain two or more functions simultaneously over a single carrier channel. There are several advantages of such a system: the effective selectivity of the receiver is increased, permitting more functions in one channel, and the effect of noise on the transmission line is reduced.

Figure 1 shows how the effective selectivity of the receiver is increased. If no tones were used, just one function could be transmitted over the carrier channel. On the basis of a total band width of only 1,650 cycles, the received side frequency furthest removed from the carrier would be 825 cycles. If tones are to be received, but no voice communication, it would be practical to use frequencies of 150, 209, 290, 403, 560, and 778 cycles per second, which amounts to 38 per cent separation between tones. With such a separation, audio filters of sufficient selectivity to pass one tone and reject all others can be designed readily. If one tone per function is allowed, this provides for six functions in a single carrier channel where only one was available without tones. Thus the effective selectivity of the receiver has been increased six times.

In addition, it is better to obtain extreme selectivity with audio filters rather than with highly selective carrier-frequency circuits. When the figures in the preceding paragraph are used with six-tone pass bands and six corresponding no-pass bands between zero and 825 cycles, a 778-cycle tone filter can have a pass band approximately 90 cycles wide. Then the 778-cycle oscillator may have a drift of ±45 cycles or ± six per cent and still remain within the filter pass band. However, to maintain the same effective selectivity with carrier-frequency circuits, the frequency drift could not exceed ±0.045 per cent (±45 cycles at 100 kilocycles). Therefore, the stability of the carrier circuits need not be as great when tones are used as would be required to obtain the same effective selectivity with unmodulated carrier.

Noise on a power transmission line covers a wide frequency range, but the selective circuits of the receiver permit the reception of only the noise components in the channel to which it is tuned. When no tones are used, it is advantageous to adjust the receiver to accept only a narrow band of frequencies, as the line

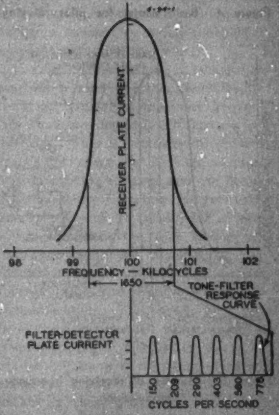

Figure 1. Typical receiver and tone-filter response curves

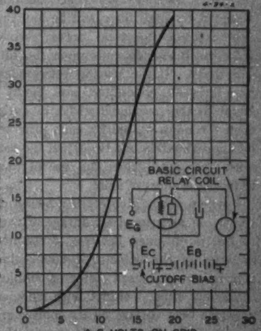

Figure 2. Dynamic characteristic of a biased detector

Paper 44-34, recommended by the AIEE joint subcommittee on power-system applications of carrier current for presentation at the AIEE winter technical meeting, New York, N. Y., January 24–28, 1944. Manuscript submitted November 12, 1943; made available for printing December 6, 1943.

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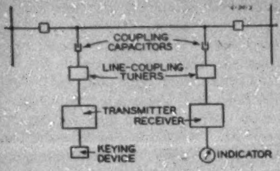

Figure 3. Simple carrier telemetering

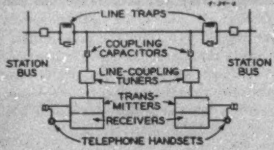

Figure 4. Basic carrier for pilot relaying

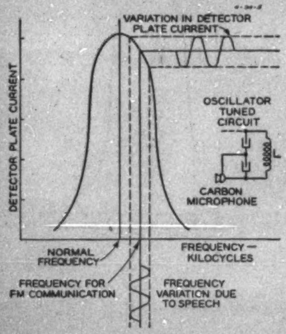

Figure 5. Method of receiving frequency modulation

noise received is proportional to the receiver band width. If the receiver output feeds six tone filters, then the noise accepted by each filter will be less than one sixth of the total noise received. How much less depends upon the filter selectivity. If the spacing between any two adjacent tone-filter pass bands is equal to the pass-band width in cycles, then the noise passed by each filter will be approximately one twelfth of the total noise received. Thus the use of tones reduces the effect of noise.

Further, if the filter feeds a biased detector, an additional advantage of three to one is obtained because of the nonlinearity of the detector dynamic characteristic. A biased detector employs a vacuum tube operated with sufficient negative bias voltage on its control grid to cut off the flow of plate current when no signal is being received. When an a-c voltage is applied to the grid, plate current flows on the positive half cycles. A plot of d-c plate current versus a-c grid voltage under operating conditions is known as the dynamic characteristic of the tube circuit. A typical curve is given in Figure 2. Reference to the curve shows that if a noise level of five volts is applied to the detector grid, the tube plate current is two milliamperes; if the noise plus signal is ten volts, the plate current is ten milliamperes. A relay adjusted to operate at six or eight milliamperes will not be operated by a noise voltage of five volts, yet reliable operation will be obtained on a total noise plus signal voltage of ten. This is a major advantage of tone-modulated carrier as it allows satisfactory reception of a signal through noise of approximately the same magnitude.

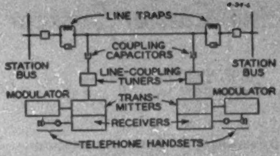

Figure 6. Carrier relaying plus modulator for communication

Expansion of a Carrier System

The following paragraphs trace the expansion of a power-line-carrier system from the simplest single-purpose arrangement to a complex installation which will provide several functions simultaneously in addition to carrier relaying. Figure 3 shows the simplest arrangement which will provide for telemetering a single quantity over a carrier channel. At the sending end, a transmitter is coupled through a line-tuning unit and coupling capacitor, to the power line. In one system, the transmitter is turned on and off, or "keyed," by the telemetering device at a rate governed by the quantity being measured. In another system, the impulse rate remains constant, but the duration of the impulse varies. The carrier-frequency impulses so produced travel along the transmission line to the receiving end which employs similar coupling and tuning equipment, and, in addition, a receiver tuned to the transmitted frequency. The received impulses indirectly energize an indicating or recording instrument, giving a reading proportional to the quantity being measured at the transmitting end.

Line traps are generally unnecessary when carrier relaying is not included but may be used if the line loss between stations and the number of paths over which the carrier energy can divide are too great. Traps may also be used if different conditions of line switching cause wide variations in carrier loss between stations. However, this will not always eliminate signal variation as an appreciable fraction of the total carrier energy can flow in the untrapped phases of the transmission line. This energy goes on into other line sections and may cause increased loss because of reflection or absorption. Three-phase trapping will eliminate such a condition, but the selection of a different carrier frequency usually obviates the necessity of inserting traps in all three phases. Energy absorbed by a parallel line is also a potential cause of increased loss, but this, too, can be minimized by changing the frequency.

Figure 4 illustrates the basic carrier elements required for pilot relaying. The carrier signal for relaying is used to prevent the protective relays from tripping the breakers in a given line section for a fault external to that section. Carrier is transmitted from the station where fault power leaves the line section and blocks the relays at the station where the fault power enters the line section. Consequently, a transmitter, receiver, and suitable line-tuning and coupling equipment are located at each end of the line section to provide correct operation for external faults in either direction. Line traps are located between the coupling capacitor and the station bus in the phase wire to which carrier is coupled. From a relaying standpoint, the main function of a line trap is to prevent short-circuiting the carrier signal upon the occurrence of a ground fault just external to the line section on the phase wire to which carrier is coupled. Without the trap, such a fault might short-circuit the carrier signal and cause incorrect relay operation.

Supervisory control or telemetering of a single quantity in either direction can be provided in addition to relaying by keying the transmitter as before. The keying device for the added function is placed in parallel with the relay contacts which start carrier by closing the cathode circuit of the tubes in the transmitter. When high-rate telemetering is used, it is desirable to insert between the keying device and the carrier transmitter an electronic link which reduces to a fraction of a milliampere the current the telemetering contact is required to make and break in controlling carrier. Longer contact life and less maintenance

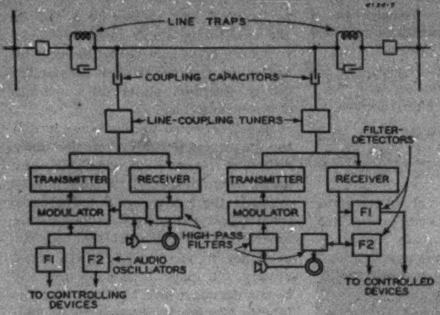

Figure 7. Audio tones added to carrier relaying

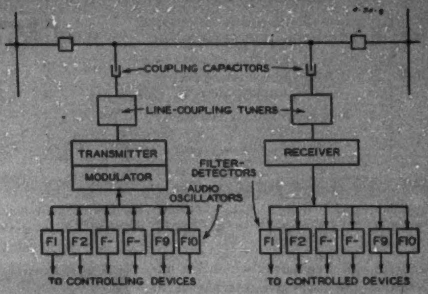

Figure 8. Use of tones to provide up to ten remotely controlled functions

are the advantages of such an electronic link.

Emergency voice communication between the carrier equipments can be effected by connecting the handset microphone across a low impedance section of the oscillator tuned circuit. Variation in the microphone resistance due to speech results in frequency modulation of the transmitted carrier. To receive frequency modulation, the carrier receiver must be capable of producing an output voltage or current proportional to the instantaneous change in frequency of the transmitter. A normal relaying signal receiver can be used satisfactorily for this application for the following reasons: When the microphone is connected into the oscillator tuned circuit, the resonant frequency of the circuit is changed by a small percentage. The receiver, which is normally tuned exactly to the transmitter frequency, is now slightly off resonance, as indicated in Figure 5, and is operating on the sloping portion of its resonance curve. When the transmitted frequency shifts back and forth because of speech, the detector plate current varies accordingly, and an audible signal is produced in the handset receiver.

This is probably one of the oldest forms of modulation, but it is still applicable, as it provides intelligible communication with a minimum of additional parts.

If communication between stations is desired from the switchboard panel, a modulator may be added to amplitude-modulate the carrier signal. Figure 6 illustrates an assembly which provides for carrier relaying and "push-to-talk" voice communication over a line section. It can be used also for telemetering a single quantity. The systems diagrammed in Figures 4 and 6 can be used for only one function at a time. If telemetering is used, it must be suspended in order to talk over the carrier channel, and, of course, during a fault, the protective relays take precedence over all other functions in controlling carrier.

When it is desired to have several functions on a single carrier channel simultaneously audio tones can be used to advantage. For example, consider an installation of carrier relaying where voice communication and telemetering of two quantities are desired. In order to transmit two quantities without interference, the measuring device associated with each controls a separate audio tone oscillator which amplitude-modulates the carrier signal. At the receiving end, the modulated carrier is fed into a detector tube to separate the audio from the carrier. The audio is then fed into filter circuits, each of which is tuned to pass one of the tone frequencies. The output voltage of each filter is applied to the grid of a vacuum tube operated as a biased detector, the operation of which was previously explained. An auxiliary relay in the plate circuit of the "filter-detector" controls the telemetering indicating device. Figure 7 illustrates such a system which can be used for telemetering two quantities as explained heretofore, for load control, or for remote tripping. For load control, the two audio tones provide raise and lower impulses to the governor of a generator; for remote tripping, the contacts of the two filter-detector auxiliary relays are connected in series to energize the breaker trip coil. To initiate a remote-tripping operation, both audio tones are transmitted (started either manually or by protective relays), and both filter-detectors at the receiving end are energized, thus completing the breaker trip circuit. The purpose of using two tones in this manner is to reduce the possibility of an incorrect breaker operation because of line noise or other disturbance producing a momentary signal which contains one of the tone frequencies.

If voice communication is included, a precaution must be taken to eliminate from the transmitter the voice frequencies which are identical with the tone frequencies. This is necessary to prevent speech from energizing the filter detectors at the receiving terminal and can be accomplished by inserting between the microphone and the modulator a filter which eliminates the tone frequencies from the speech input. By selecting tone frequencies below 500 cycles, several tones may be used simultaneously with communication. The speech filter can be of the high-pass type to attenuate all frequencies below 500 cycles 40 to 50 decibels so there is no danger of their energizing a filter detector. The elimination of these frequencies does very little to impair the intelligibility of speech.

In those cases where the tones are transmitted infrequently, as for remote tripping, the high-pass filters are not necessary in the telephone receiver circuit because the interference with speech reception is of short duration. However, for telemetering and other functions requiring the tones to be impulsed continuously, filters should be provided in the telephone receiver circuit. The aforementioned features are indicated in Figure 7.

Figure 8 illustrates extensive use of tones without communication or relaying and will provide up to ten remotely controlled functions over a single carrier channel. As all ten tones may be transmitted simultaneously, each tone must be adjusted to modulate the carrier signal not more than ten per cent to prevent overmodulation. This would cause severe distortion and generation of

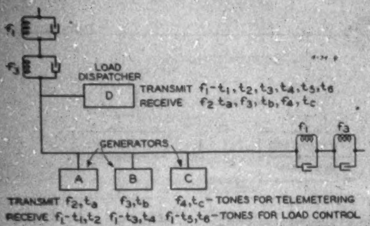
f1
f3
LOAD DISPATCHER
D
TRANSMIT f1-t1, t2, t3, t4, t5, t6
RECEIVE f2 ta, f3, tb, f4, tc
f1
f3
GENERATORS
A
B
C
TRANSMIT f2, ta f3, tb f4, tc – TONES FOR TELEMETERING
RECEIVE f1-t1, t2 f1-t3, t4 f1-t5, t6 – TONES FOR LOAD CONTROL

Figure 9. Extensive use of tones for telemetering and load control

$f_2=(N-1.5)$ kilocycles
$f_3=(N)$ kilocycles
$f_4=(N+1.5)$ kilocycles

spurious side frequencies which are undesirable and may give interference in adjacent channels.

Since the percentage modulation per tone must be decreased when the number of tones is increased, the carrier sideband power in each tone is also decreased. Therefore, a more sensitive receiver is required for this application than for one where unmodulated carrier is received (assuming the same carrier power in both cases). Also, the receiver must have a low distortion characteristic so as to reproduce the tone frequencies without introducing unwanted harmonics. Ten audio tones can be spaced in a band from 150 to 3,000 cycles with a 38 per cent frequency separation between consecutive tones. To receive a carrier signal modulated with tones up to 3,000 cycles, the receiver must be capable of receiving a frequency band six kilocycles wide, and rejecting those outside the six-kilocycle band which may belong to another carrier channel. A simple receiver such as that used for relaying is not suitable for this application of tones. However, a broad-band superheterodyne receiver fulfills the preceding requirements of sensitivity, selectivity, and band width. Such a receiver should be equipped with automatic volume control to deliver sufficient audio voltage to the tone filters over a wide range of carrier input signal to allow for varying system conditions. For applications on lines with a high carrier loss, a receiver sensitivity great enough to provide dependable operation through a loss of 70 decibels is desirable.

A specific application of extensive use of tones for telemetering and load control is shown in Figure 9. The problem here is to keep the total load divided properly among three generators A, B, and C, and use as few carrier channels as practical. The load on each generator is to be telemetered to the load dispatcher at D, where load-control equipment sends raise and lower impulses back to the generating stations to keep the load divided as desired. These functions are accomplished thus: The carrier transmitters at stations A, B, and C are set at frequencies 1.5 kilocycles apart, and each is modulated with a different tone frequency for telemetering its generator load back to station D. The receiver at D is adjusted to receive a band wide enough to include the frequencies sent from A, B, and C, and the carrier side frequencies produced by the tones. Filters on the receiver at D separate the audio tones t_a, t_b, and t_c. The impulses provided by these tones operate the load-control equipment. Six audio tones are used to transmit the raise and lower load-control impulses from D to the generating stations A, B, and C on a single carrier frequency f_1 which may be widely different from f_2, f_3, and f_4. At each generating station, a receiver is tuned to f_1, and two tone filters pass the raise an lower impulses for that station. Only two carrier channels are needed; one channel 4.5 kilocycles wide to transmit f_2, f_3, and f_4, and another for f_1 with its six modulating audio tones. Double-frequency traps may be put in each end of the line if needed to allow satisfactory carrier reception under all conditions of system switching.

Figure 10. Typical power-line-carrier assembly for relaying, remote tripping, and point-to-point communication

It will be noted in this example that the frequency separation of the transmitters at stations A, B, and C will cause major heterodynes of 1.5 and 3 kilocycles in the receiver at D. The audio tones t_a, t_b, and t_c must be sufficiently lower than 1.5 kilocycles that, for the maximum frequency drift of the transmitters, the major beat notes will still be far enough above the highest audio tone to produce no interference. There will be several minor heterodynes in the filter frequency band but of such low amplitude as to cause negligible interference.

The Equipment

From the preceding examples of the use of power-line carrier, it is evident that the equipment must be of flexible electrical and mechanical design to be readily adaptable to widely varying applications. Rack-and-panel construction provides flexibility, and each major piece of equipment is constructed as a separate unit on standard 19-inch-wide relay rack panels. All external connections are brought out to terminal strips on the rear of each unit to facilitate interconnections between units and still maintain a pleasing appearance.

Figure 10 illustrates a typical installation which combines pilot relaying, two tone functions, and point-to-point communication. The entire rack assembly can be swung out of the cabinet on hinges to facilitate inspection and provide accessibility to all components.

Conclusions

1. Audio tones provide an effective way of increasing the usefulness of a carrier channel.

2. Simultaneous operation of several auxiliary functions over a single carrier channel can be provided without interference by using a separate tone for each function.

3. Satisfactory carrier transmission through a higher noise level can be realized by using tone modulation.

4. Telephone communication can be used simultaneously with tone-operated functions by using suitable filters to separate the tone and voice frequencies.

References

1. POSSIBILITIES AND METHODS OF EXTENDING A CARRIER-CURRENT-RELAY CHANNEL TO OTHER

Some Aspects of Inductance When Iron Is Present

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Synopsis: It is the purpose of this paper to examine various definitions of inductance based on the application in which the term is used. Comparisons are made between the definitions as a function of magnetomotive force and flux density for the special case of dynamo-grade (2.75 per cent) silicon steel. The effects of hysteresis and eddy currents are discussed. Consideration is confined to iron magnetic circuits in which no air gap exists, so that practical applications of conclusions drawn are limited somewhat.

THE usual definition of inductance is based on the assumption that the saturation curve is a straight line. Other assumptions, which are implied though not necessarily expressed, are that there is no residual flux, no hysteresis, and no eddy-current loss. For these conditions all definitions yield the same value of a constant inductance.

There are, however, many applications where solid iron is used in the magnetic structure. In the majority of these applications it is economically necessary to run the iron at a density which makes the saturation curve depart considerably from that of a straight line. Under these practical conditions eddy currents, hysteresis, and saturation all contribute to make a definition which assumes that constant inductance often be considerably in error.

The importance of this subject will be appreciated when it is recognized that the actual duty on the contact tips of a d-c contactor may be several times greater than would be expected from calculated values of the load inductance.

Use of the Term "Inductance"

The term "inductance" is used in connection with many circuits and phenomena. Its use in electric machinery often has been defined and elaborated on. There are, however, many instances, particularly in the industrial-control field, where a word is required to express a certain characteristic of a circuit, and that omnipresent word is "inductance." The following examples may be cited:

1. The build-up of current in the shunt field of a d-c machine, d-c contactor, relay, or brake is stated to depend on the time constant or ratio of inductance to resistance of the circuit.

2. The number of operations of which a contactor is capable depends to a great extent on the stored energy in the electric load which it is handling. In the case of a d-c contactor a large amount of this stored energy must be dissipated in the arc with consequent burning of tips, arcing horns, and chutes. The energy stored is stated to be one half of the product of inductance times current squared.

3. The rate of decay of d-c fields often is important when a field must be reduced rapidly or "killed." Here again decay is dependent supposedly on the time constant of the circuit.

4. When a contactor closes the tendency for the tips to weld or freeze depends on the rate at which the current builds up, and this usually is considered to be inversely proportional to the inductance of the circuit.

5. When a contactor interrupts an a-c circuit, the recovery voltage, after a current zero, rises at a rate which is dependent on the circuit inductance and capacitance.

6. In obtaining the inductance of an iron-core reactor it is common practice to read volts, amperes, and watts and to calculate the reactance and inductance from the data so obtained.

It will be shown in the following that for an iron-cored circuit the various inductances enumerated previously may be considerably different from each other, so much so that large errors result if they are used interchangeably.

Common Definitions of Inductance

There are three definitions of L commonly used in the literature.[1] The first is in terms of flux linkages per ampere, that is

$$L=\frac{\Psi_e}{i}=\frac{N\Phi_e}{i} \tag{1}$$

In order that L be constant in equation 1, the condition must hold that flux is proportional to current. The second usual definition is to define L in terms of the back electromotive force by means of the equation

$$e=-L\frac{di}{dt} \tag{2}$$

The third definition is in terms of the energy stored in a magnetic field and is given by the equations

$$W=-\int ei\,dt=\int_0^i Li\,di=\frac{1}{2}Li^2 \tag{3}$$

Equations 1, 2, and 3 are used as the three basic definitions and give identical and constant values of L for the linear magnetization curve.

Although practically all textbooks and handbooks define inductance in one of these three ways, the majority (including the "American Standard Definitions of Electrical Terms" by AIEE) prefer to consider equation 2 as being basic.[2-6]

Equation 2 also may be stated

$$L=-\frac{e}{\frac{di}{dt}}=-e\frac{dt}{di}=N\frac{d\Phi_e}{di} \tag{4}$$

and L is defined in this way by several other authors.[6,7]

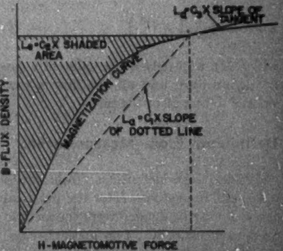

Figure 1. Geometrical interpretation of L_a, L_d, and L_e in terms of the d-c magnetization curve

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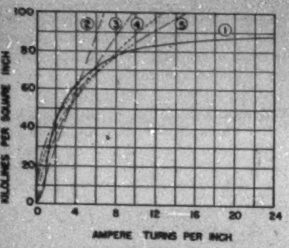


Figure 2. Comparison of the curves $H=KB^n$ with the magnetization curve of silicon steel

1. An actual curve for 2.75 per cent silicon steel
2. $H=(6.7)(10^{-2})B$
3. $H=(9.65)(10^{-3})B^{1.5}$
4. $H=(1.26)(10^{-3})B^{2}$
5. $H=(1.5)(10^{-4})B^{2.5}$

The "Standard Handbook for Electrical Engineers" edited by Knowlton lists both equations 2 and 3 as fundamental, calling L the coefficient of proportionality in each case.[8]

Equation 1 seems to be considered fundamental mainly by men concerned with the analysis of rotating machinery.[9-11]

Some textbooks redefine their fundamental magnetic concepts entirely when they change from air-core to iron-core circuits.[12] One author[13] expresses the opinion that L should not be introduced at all when the permeance of the circuit is variable.

Geometric Definition of Inductance

Another usual method of defining inductance[10] is to express it as a function of the physical dimensions. These include the area, length, and permeability of the flux paths, and dimensions and turns in the coils. This leads to the expression

$$L=(\text{constant})\left(N^2\frac{\mu A}{l}\right) \tag{5}$$

which is useful only if the value of μ can be determined, and all the flux is considered to link all of the turns.

Definitions From Measurements

Besides these definitions (from which it is hard to evaluate L for the practical case), L may be defined from the method used to measure it. Two methods commonly used are

1. A-c measurements

$$E=I(R+jX_L)=I(R^2+\omega^2L^2)^{1/2} \tag{6}$$

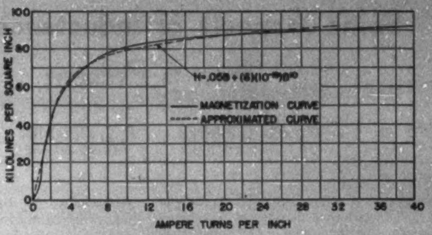


Figure 3. Comparison of the d-c magnetization curve for 2.75 per cent silicon steel with the equation $H=0.05B+(6)(10^{-19})B^{10}$

So

$$L=\frac{1}{\omega}\left(\frac{E^2}{I^2}-R^2\right)^{1/2} \tag{7}$$

or if

$R\ll\omega L$

$$L=\frac{1}{\omega}\frac{E}{I} \tag{8}$$

2. Oscillogram of current change

$$E=iR+L\frac{di}{dt} \text{ for an } R\text{–}L \text{ series circuit} \tag{9}$$

So

$$i=\frac{E}{R}\left(1-e^{-\frac{R}{L}t}\right) \text{ for current rise} \tag{10}$$

and

$$i=I_0e^{-\frac{R}{L}t} \text{ for current decay if } L \text{ is constant} \tag{11}$$

From these equations

$$L=\frac{Rt}{\log_e\frac{I_f}{I_f-i}} \text{ for build-up} \tag{12}$$

and

$$L=\frac{Rt}{\log_e\frac{I_0}{i}} \text{ for decay} \tag{13}$$

Summary of Definitions

In order to differentiate between the quantities denoted by the common term "inductance" in the inductances taken from the preceding definitions, the following subscripts and names have been given arbitrarily to L:

L_a = apparent inductance = Ψ_0/i from equation 1

L_d = differential inductance = $\frac{-e}{(di/dt)}$ = $N\frac{d\Phi_0}{di}$ from equations 2 and 4

L_e = inductance based on energy = $\frac{2W}{i^2}$ from equation 3

L_g = inductance based on geometry = (constant) $\left(N^2\frac{\mu A}{l}\right)$ from equation 5

L_{ac} = inductance based on rms readings = $\frac{1}{\omega}\frac{E}{I}$ from equation 8

L_{tb} = inductance based on time for current build-up = $\frac{Rt}{\log_e\frac{I_f}{I_f-i}}$ from equation 12

L_{td} = inductance based on time for current decay = $\frac{Rt}{\log_e\frac{I_0}{i}}$ from equation 13

Graphical Interpretation (Figure 1)

Figure 1 shows a magnetization curve plotted as B versus H. Since for a given magnetic circuit B is proportional to Ψ_0 and H to i, then

$$L_a=\frac{\Psi_0}{i}=(\text{constant})\left(\frac{B}{H}\right)$$

so that L_a is proportional to the slope of the straight line drawn from the point of

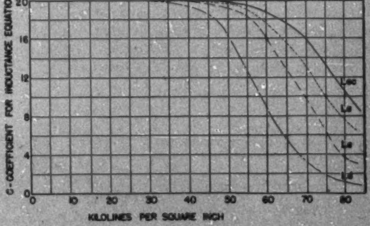


Figure 4. Variation of different inductances as a function of flux density

operation to the origin. This definition of L_s is very similar to the usual definition of μ which is defined as the ratio of B at a point to the H at that point.

Similarly

$$L_d = N\frac{d\Phi_s}{di} = (\text{constant})\left(\frac{dB}{dH}\right)$$

is proportional to the slope of the magnetization curve itself at a point and is analogous to μ_d, the differential permeability.

Also, since

$$W = -\int ei\,dt = \int i\frac{d\Psi}{dt}\,dt = \int i\,d\Psi$$
$$= (\text{constant})\int H\,dB$$

the energy stored in the magnetic field varies directly as the area between the magnetization curve and the vertical axis. Then L_e is proportional to this area for any given current.

Analytical Equivalent of a Magnetization Curve

An accurate analytical expression for the saturation curve is quite unwieldy, if it is desired to express B as a function of H. In order to approach a comparison of the different definitions of L mathematically, four flux versus current curves, namely $H = KB^n$ where $n = 1$, 1.5, 2, and 2.5 were assumed. These are shown in Figure 2 compared with the magnetization curve for 2.75 per cent silicon steel. A reactor was assumed to consist of N turns on a closed core of length l and cross-sectional area A. Resistance, leakage, and hysteresis were neglected. The inductance was calculated as a function of the current for L_s, L_d, and L_e, and as a function of rms current and rms voltage for L_{ac} for each of the assumed B–H curves. See Appendixes II, III, IV, and V.

Results are shown in Table I. It should be noted that for L_s, L_d, L_e, and L_{ac} as a function of current, the expressions vary only in their numerical coefficient. L_s is the greatest, and L_d is the least. It is also interesting to note that, if L_{ac} is written in terms of voltage, L_{ac} becomes a function of frequency—a rather surprising result.

A more accurate approximation to the saturation curve is given by $H = K_1B + K_2B^{10}$ which is shown in Figure 3. Because of the difficulty involved in solving this equation for dB/dH or for B as a function of H, the inductances are compared in terms of B and B_{rms} (Table I). These equations are shown plotted against B in Figure 4 and against H in Figure 5. The coefficient C is the value by which the expression $(N^2A/l)10^{-8}$ must be multiplied in order to obtain L in henrys. See Appendix II*B*.

For the case $H = K_1B + K_2B^{10}$ there does not seem to be a definite relationship between the different inductances, except that they all decrease quite rapidly as soon as the knee of the B–H curve is reached. Differential L drops the fastest and to the lowest value.

An air gap in the magnetic circuit would increase K_1 in the foregoing equation. Its effect would be to lower the C–H curves and to shift them to the right. All values of inductance would be lowered except L_e which at certain currents may be higher with an air gap in the circuit than when the core is closed.

Inductance When Hysteresis Is Present

Figure 6 shows the variation of L_d and L_e with current, that is, with H, as a hysteresis loop for Nicaloi steel is traced. It should be noted that the coefficient C in the equation

$$L_d = C\frac{N^2A}{l}10^{-8} \tag{14}$$

varies from 4 to 175 over every half-cycle.

For L_e, C varies from minus to plus infinity which is a rather startling range for a quantity which is usually assumed constant. Figure 9 was drawn for Nicaloi, because it is a material which has a very low hysteresis-loop area and, therefore, is a close practical approach to a commercial material with no hysteresis. It is used extensively in transformers, reactors, and relays.

Figure 7 shows the same curves of L_e and L_d for a 2.75 per cent silicon steel. Here C in L_d varies from 0.8 to 4.7, and C in L_e, as before, goes from minus infinity to plus infinity.

Presence of Eddy Currents

Many oscillograms, taken on solid iron contactors and brakes to get current–time curves when a constant d-c voltage is applied, show a rapid initial rate of rise

Table I. Calculated Formulas of Inductance for Various Assumed Magnetization Curves

$H = f(B_s)$	$L_s = \frac{\Psi}{i}$ In Terms of Current i	$L_d = \frac{d\Psi}{di}$ In Terms of Current i	$L_e = \frac{2w}{i^2}$ In Terms of Current i	$L_{ac} = \frac{1}{\omega}\frac{E_{rms}}{I_{rms}}$ In Terms of Rms Current	$L_{ac} = \frac{1}{\omega}\frac{E_{rms}}{I_{rms}}$ In Terms of Rms Voltage
$H = KB_s^{1.0}$	$1.0\frac{N^2A}{Kl}$	$1.0\frac{N^2A}{Kl}$	$1.0\frac{N^2A}{Kl}$	$1.0\frac{N^2A}{Kl}$	$1.0\frac{N^2A}{Kl}$
$H = KB_s^{1.5}$	$1.0\frac{N^{5/3}A}{K^{2/3}l^{2/3}i^{1/3}}$	$0.67\frac{N^{5/3}A}{K^{2/3}l^{2/3}i^{1/3}}$	$0.80\frac{N^{5/3}A}{K^{2/3}l^{2/3}i^{1/3}}$	$0.94\frac{N^{5/3}A}{K^{2/3}l^{2/3}I^{1/3}}$	$0.92\frac{N^{5/2}A^{3/2}\omega^{1/2}}{KlE^{1/2}}$
$H = KB_s^{2.0}$	$1.0\frac{N^{3/2}A}{K^{1/2}l^{1/2}i^{1/2}}$	$0.50\frac{N^{3/2}A}{K^{1/2}l^{1/2}i^{1/2}}$	$0.67\frac{N^{3/2}A}{K^{1/2}l^{1/2}i^{1/2}}$	$0.91\frac{N^{3/2}A}{K^{1/2}l^{1/2}I^{1/2}}$	$0.82\frac{N^3A^2\omega}{KlE}$
$H = KB_s^{2.5}$	$1.0\frac{N^{7/5}A}{K^{2/5}l^{2/5}i^{3/5}}$	$0.40\frac{N^{7/5}A}{K^{2/5}l^{2/5}i^{3/5}}$	$0.57\frac{N^{7/5}A}{K^{2/5}l^{2/5}i^{3/5}}$	$0.88\frac{N^{7/5}A}{K^{2/5}l^{2/5}I^{3/5}}$	$0.73\frac{N^{7/2}A^{5/2}\omega^{3/2}}{KlE^{3/2}}$
	In Terms of $B_{instantaneous}$			In Terms of B_m	
$H = K_1B + K_2B^{10}$	$\left(\frac{N^2A}{K_1l}\right)\frac{10^{-8}}{1+\frac{K_2}{K_1}B^9}$	$\left(\frac{N^2A}{K_1l}\right)\frac{10^{-8}}{1+10\frac{K_2}{K_1}B^9}$	$\left(\frac{N^2A}{K_1l}\right)\frac{\left(1+0.18\frac{K_2}{K_1}B^9\right)10^{-8}}{1+2\frac{K_2}{K_1}B^9+\left(\frac{K_2}{K_1}\right)^2B^{18}}$	$\left(\frac{N^2A}{K_1l}\right)10^{-8}\left[1+0.94\frac{K_2}{K_1}B_m^9+0.35\left(\frac{K_2}{K_1}\right)^2B_m^{18}\right]^{-1/2}$	

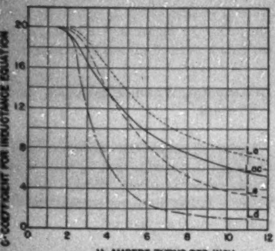


Figure 5. Variation of different inductances as a function of magnetomotive force

of current. This effect is attributed sometimes to remanent flux in the device and sometimes to eddy currents. Figure 8 is a copy of a typical oscillogram taken on a contactor having a magnetic circuit about 11 inches in length and average cross section of one square inch with a 0.015-inch air gap in the circuit. Tests taken of current build-up with both positive and negative remanent flux left in the circuit by previous operations showed practically no change in initial rise.

The values of inductance at different currents calculated from the oscillogram in a step-by-step process using the equation

$$L_a = \frac{E-Ri}{\dfrac{di}{dt}} \qquad (15)$$

are shown in Figure 9.

Curves 1 and 2 of Figure 9 show the variation of L with current build-up and curve 3 with current decay (calculated from an oscillogram). It should be noted that curve 3 reads from right to left with respect to time. Although direct comparisons of curves 1 and 3 are impossible because of the hysteresis loop, it is still significant to notice the high inductance exhibited by curve 3 at zero current compared with the inductance shown by curve 1. This probably is due to the difference in the rate of current in the coil. In the former case the d-c arc was going out slowly, while in the latter case a sudden voltage inducing large eddy currents was applied.

Other data, not reproduced here, in which the current and flux rise in a series brake magnet were measured, have shown that the time required for the flux in the air gap to reach its final value is about ten times as long as the time required for the current to build up. This lag is attributable to the demagnetizing action of the eddy currents and, of course, affects the inductance.

Inductance Based on Build-up and Decay of Current

Figure 10 shows calculated curves for the increase of current with time when a constant voltage is applied to a 2.75 per cent silicon-steel-core reactor, and saturation is taken into account, but hysteresis and eddy currents are neglected. Four such curves for different applied voltages are plotted. These curves were obtained by a step-by-step integration of the equation

$$E = Ri + L_a\frac{di}{dt} \qquad (16)$$

where the equation $H = 0.05B + (6)\times(10^{-19})\times B^{10}$ was used instead of the true

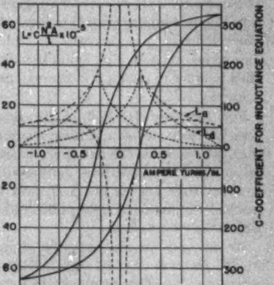


Figure 6. Variation of L_a and L_d over a complete cycle of the hysteresis loop of Nicaloi

B–H curve of the material. This was done, so that equivalent inductances (L_{eb}) calculated from the curves of Figure 10 could be compared with those obtained by calculation from other definitions. Equivalent inductance means that value of inductance which, if constant, would allow the current to reach the same percentage of its final value in the same time that it does in Figure 10. From these curves of Figure 10 the value of time at which the current was equal to 90 per cent of its final value was read. When equation 10 was used, the value of L_{eb}, the equivalent or constant inductance, was calculated. The same was done for current equal to 63 per cent of its final value. When these calculated values of L_{eb} were used, the curves 2 and 3 of Figure 12 were plotted. The difference between L_a, the usual definition for inductance for a d-c field, and the constant inductance, which should be used to give the true time of build-up, is shown clearly.

Curves 1 and 4 of Figure 12 are calculated from the decay curve of Figure 11. These current-decay curves were obtained in the same way the current-rise curves were found, that is, by a step-by-step integration of equation 16 when $E=0$ for the magnetization curve $H = 0.05B + (6)(10^{-19})B^{10}$.

It should be noted that L_a in Figure 12 falls between curves 1 and 3 for the 90 per cent case and also between curves 2 and 4 for the 63 per cent case, that is, the apparent inductance is somewhere between the inductance calculated from build-up and decay.

If currents which are closer to 100 per cent of final are chosen, curve 3 will be lower and curve 1 will be higher, showing more and more error between apparent inductance and the constant inductance which gives the correct build-up or decay time.

Experimental Verification

An experimental verification of these theoretical curves is shown in Figure 13 which is a copy of an oscillogram. This was taken to measure the inductance of the field of a 50-horsepower synchronous motor, so that an equivalent circuit could be built in order to test the interrupting ability of switches of different ratings. From the slope of the current–time curve the inductance was computed by equation 14 and is shown plotted in Figure 14. It may be noted that the

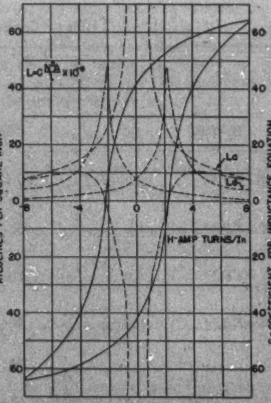


Figure 7. Variation of L_a and L_d over a complete cycle of the hysteresis loop of 2.75 per cent silicon steel

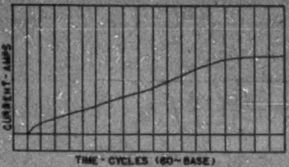


Figure 8. Traced copy of an oscillogram showing current build-up with constant applied voltage in a d-c solid-iron contactor with 0.015-inch air gap

current–time curve is very similar to those shown in Figure 10. The inductance of this machine had been given as 12 henrys; the oscillogram showed a range from nine to three henrys.

Conclusions

1. Although comparisons have been made in this paper between various definitions of L on the basis of a closed circuit and silicon steel, similar results can be obtained as readily for any other steel and circuit containing an air gap.

2. The assumption, that inductance is a constant in an iron magnetic circuit where the flux density is of the usual practical value, and the air gaps are small, leads to large errors. Saturation cannot be neglected merely to obtain a mathematical solution of a problem, for the solution then bears little relation to the actual case.

3. The true inductance of a circuit, in the sense of a back electromotive force, is the differential inductance. There is much work to be done to calculate differential inductance for magnetic circuits which have air gaps. Results probably can be expressed usefully in terms of mils of air gap per inch of iron. Such data on machines are necessary, if accurate control of those machines is to be precalculated.

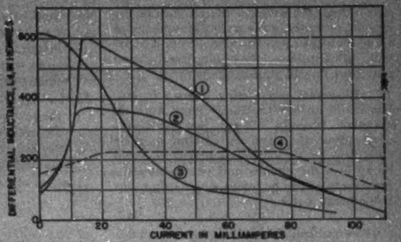
4. Observation of Figures 4, 6, and 12 shows the wide variations in inductance, depending on the method of calculation or measurement. The particular value to be used depends on the application to which it is applied.

5. The statement, that inductance is a function of the physical dimensions of a circuit and of μ, throws the whole definition into an evaluation of the value of μ and is for that reason of little use.

6. The widespread use of a time constant based on flux linkages per ampere, that is L_a, often leads to results considerably in error. With the great increase in speeds of voltage response called for on a large variety of industrial-control applications, such as variable-voltage planer drives or amplidyne-forced steel-mill motors, it is becoming increasingly necessary for the circuit designer to have more information regarding current build-up in the d-c fields. For accurate field control as a function of time, it is necessary to know an equivalent inductance (L_e) which will give the correct build-up time.

Figure 9 (right). Variation of L_d with current for the d-c contactor of Figure 8

1. L_d calculated from current build-up after contactor had carried current in the negative direction. Air gap is 0.015 inch
2. L_d calculated from current build-up after contactor had carried current in the positive direction. Air gap is 0.015 inch
3. L_d calculated from an oscillogram of current decay. Air gap is 0.015 inch
4. Same as 1 but with an air gap of 0.375 inch



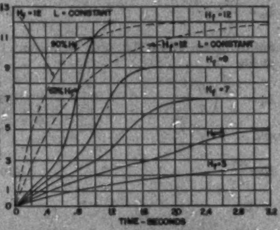


Figure 10. Current–time build-up curves calculated from $H=0.05B+(6)(10^{-19})B^{10}$ for various applied voltages compared with theoretical constant-inductance build-up curves

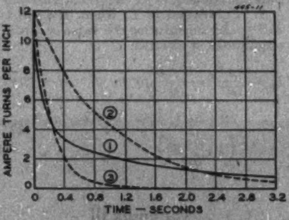


Figure 11. Current-decay curves

1. Current–time-decay curve for the material given by $H=0.05B+(6)(10^{-19})B^{10}$
2. H-versus-time curve for the constant inductance (L_{td}) which crosses curve 1 at 10-per cent of its original value
3. H-versus-time curve for the constant inductance (L_{td}) which crosses curve 1 at 37 per cent of its original value

7. In specifying the interrupting ability of contactors which handle motor loads, it would seem advisable, in order to obtain comparative data, to set up L/R ratios for air-core reactors rather than to specify motor loads. Much work still remains to be done to identify the effect of a variable inductance load on both a-c and d-c interruption, so that equivalent air-core loads can be used for test purposes.

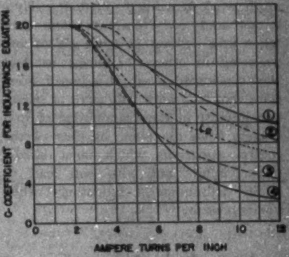


Figure 12. Variation of L_{td} and L_{tb} with magnetomotive force

1. L_{td} for decay time at which $i=10$ per cent I_o
2. L_{tb} for build-up time at which $i=63$ per cent I_f
3. L_{tb} for build-up time at which $i=90$ per cent I_f
4. L_{td} for decay time at which $i=37$ per cent I_o

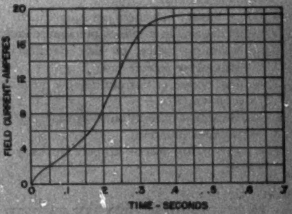


Figure 13. Traced copy of an oscillogram showing current build-up versus time at constant applied voltage for a 50-horsepower synchronous-motor field

8. In the majority of d-c circuits containing solid iron, eddy currents affect the initial rate of current rise, so that a calculated inductance, such as L_a, is not a dependable criterion for severity of the circuit with respect to tip welding. To obtain comparative data air-core inductances should be specified.

9. The authors have made no investigations as yet on a circuit which contains motor or reactor loads to find the inductance

which, together with the capacitance of the system, determines the natural frequency of the circuit and so critically affects a-c circuit interruption. Such an investigation may lead to still another inductance.

10. Electrical engineering courses in college could include profitably more work on magnetic circuits. Very few textbooks even admit the presence of iron in their treatment of the classical current rise and decay in a d-c circuit, and yet in industry few examples can be found where iron is not a dominant factor.

It is realized that the foregoing analysis is by no means complete, and that much work remains to be done. Hysteresis and eddy currents have been taken into account in some places and neglected in others. Leakage has not been considered at all. It is hoped, however, that this work will serve to stimulate interest in the examination of iron circuits from the point of view of the control apparatus which must handle those circuits and also will lead to AIEE definitions of inductance when iron is present.

Appendix I. Nomenclature

L = inductance in henrys
e = voltage at time t
i = instantaneous value of current
Ψ_o = flux linkages—webers times turns
Ψ = flux linkages—kilolines times turns
t = time—seconds
W = energy—joules
I = rms current
E = rms voltage or d-c voltage
Φ_o = flux in webers
Φ = flux in kilolines
B_o = flux density in webers per square inch
B = flux density in kilolines per square inch
B_m = maximum value of flux density in kilolines per square inch
I_o = initial value of current for d-c current decay $= E/R$
I_f = final value of current build-up case $= E/R$
R = resistance in circuit
H = magnetizing force—ampere turns per inch
H_f = final value of magnetomotive force in a d-c circuit
l = length of the magnetic path in inches
A = cross section of the magnetic path in square inches
L_a, L_d, L_e, L_g, L_{ac}, L_{tb}, L_{td} are defined in the text in the paragraph "Summary of Definitions"
$\omega = 2\pi f$ where f is the frequency
μ = permeability
A = area of an iron circuit in square inches

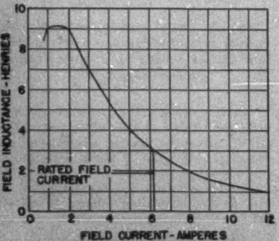


Figure 14. Inductance (L_d) of the synchronous-motor field calculated from Figure 13

Appendix II

A. Calculation of L_a when $H = KB_o^n$. From equation 1, which is the defining equation for L_a

$$L_a = \frac{N\Phi_o}{i} = \frac{NAB_o}{i} \tag{17}$$

Substituting $H = KB_o^n$ into equation 17 yields

$$L_a = \frac{NA}{i}\left(\frac{H}{K}\right)^{\frac{1}{n}} \tag{18}$$

and

$$H = \frac{Ni}{l} \tag{19}$$

so that

$$L_a = \frac{NA}{i}\left(\frac{Ni}{lK}\right)^{\frac{1}{n}} \tag{20}$$

Substitution of $n = 1.0$, 1.5, 2.0 and 2.5, into equation 20 gives the formulas for L_a shown in Table I.

B. Calculation of L_a when $H = K_1B + K_2B^{10}$. By substituting the value of i from equation 19 in equation 17

$$L_a = \frac{N^2AB_o}{lH} = \frac{N^2AB}{lH}10^{-8} \tag{21}$$

$$= \frac{N^2A\,10^{-8}}{lK_1\left(1+\frac{K_2}{K_1}B^9\right)} \tag{22}$$

Appendix III

A. Calculation of L_e when $H = KB_o^n$

$$W = \int ei\,dt \text{ and } e = NA\frac{dB_o}{dt}$$

so that

$$W = NA\int i\,dB_o$$

Now

$$B_o = \left(\frac{H}{K}\right)^{\frac{1}{n}}$$

and

$$dB_o = \frac{1}{n}\frac{H^{\left[\frac{1}{n}-1\right]}}{K^{\frac{1}{n}}}\,dH \tag{23}$$

$$dB_o = \frac{1}{nK^{\frac{1}{n}}}\left(\frac{iN}{l}\right)^{\left[\frac{1}{n}-1\right]}dH \tag{24}$$

and

$$W = \frac{NA}{nK^{\frac{1}{n}}}\left(\frac{N}{l}\right)^{\frac{1}{n}}\int i^{\frac{1}{n}}di$$

$$= \frac{AN^{\left[1+\frac{1}{n}\right]}}{K^{\frac{1}{n}}\,l^{\frac{1}{n}}}\left(\frac{i^{\left[1+\frac{1}{n}\right]}}{n+1}\right) \tag{25}$$

Since

$$W = \frac{1}{2}Li^2$$

then

$$L = \frac{2W}{i^2} = \frac{2AN^{\left[1+\frac{1}{n}\right]}\,i^{\left[\frac{1}{n}-1\right]}}{K^{\frac{1}{n}}\,l^{\frac{1}{n}}\,(n+1)} \tag{26}$$

Substitution of $n = 1.0$, 1.5, 2.0, and 2.5 in equation 26 yields the formulas shown for L_e in Table I.

B. Calculation of L_e when $H = K_1B + K_2B^{10}$

$$W = eidt = lA\int HdB_o$$

and

$$L = \frac{2W}{i^2} = \frac{2AN^2}{lH^2}\int HdB_o \tag{27}$$

$$= \frac{2AN^2 10^{-8}}{lH^2}\int (K_1B + K_2B^{10})dB$$

$$= \frac{AN^2 10^{-8}}{l}\,\frac{(K_1B^2 + 0.182K_2B^{11})}{K_1^2B^2 + 2K_1K_2B^{11} + K_2^2B^{20}}$$

$$= \frac{AN^2}{lK_1}\,\frac{\left(1+0.182\frac{K_2}{K_1}B^9\right)10^{-8}}{1+2\frac{K_2}{K_1}B^9 + \left(\frac{K_2}{K_1}\right)^2B^{18}} \tag{28}$$

Appendix IV

A. Calculation of L_d when $H = KB_o^n$ From equation 4 which defines L_d

$$L_d = N\frac{d\Phi_o}{di} = NA\frac{dB_o}{di} \tag{29}$$

where

$$B_o = \left(\frac{H}{K}\right)^{\frac{1}{n}} = \left(\frac{Ni}{Kl}\right)^{\frac{1}{n}}$$

Then

$$\frac{dB_o}{di} = \frac{1}{n}\left(\frac{N}{Kl}\right)^{\frac{1}{n}} i^{\left[\frac{1}{n}-1\right]} \tag{30}$$

and

$$L_d = \frac{NA}{n}\left(\frac{N}{Kl}\right)^{\frac{1}{n}}\frac{1}{i^{\frac{n-1}{n}}} \tag{31}$$

which is equal to L_a/n. Substitution of $n = 1.0$, 1.5, 2.0, and 2.5 into equation 31 gives the formulas for L_d in Table I.

B. Calculation of L_d when $H=K_1B+K_2B^{10}$

$$L_d=\frac{Nd\Phi_e}{di}=\frac{N^2A}{l}\,\frac{dB_e}{dH}=\frac{N^2A}{l}\,\frac{dB}{dH}\,10^{-8} \qquad (32)$$

$$=\frac{N^2A}{l}\,\frac{10^{-8}}{K_1+10K_2B^9}$$

$$=\frac{N^2A}{lK_1}\,\frac{10^{-8}}{\left(1+10\dfrac{K_2}{K_1}B^9\right)} \qquad (33)$$

Appendix V

A. Calculation of L_{ac} when $H=K(B_e)^n$. Assuming no resistance or hysteresis

$$L_{ac}=\frac{1}{\omega}\,\frac{E}{I}=\frac{1}{\omega}\,\frac{E_m}{(2)^{1/2}\left[\dfrac{1}{T}\displaystyle\int_0^T i^2dt\right]^{1/2}} \qquad (34)$$

Now

$$e=NA\frac{dB_e}{dt}=E_m\sin\omega t$$

when B_e is in webers per square inch, so that

$$B_e=\frac{-E_m}{NA\omega}\cos\omega t \qquad (35)$$

Now

$$i=\frac{lH}{N}=\frac{lKB_e^{\,n}}{N} \qquad (36)$$

$$=\frac{lK}{N}\left(\frac{-E_m}{NA\omega}\cos\omega t\right)^n \qquad (37)$$

When $n=1$, 1.5, 2, and 2.5 is substituted in equation 36, and equation 34 is evaluated, the results shown in Table I for L_{ac} in terms of E_m are obtained.

Substituting $\omega LI=E$ in the equations so derived yields the formulas shown for L_{ac} in terms of I in Table I.

B. Calculation of L_{ac} when $H=K_1B+K_2B^{10}$

$$L_{ac}=\frac{1}{\omega}\,\frac{E}{I}=\frac{NAB_m10^{-8}}{\sqrt{2}\,I}=\frac{0.707NAB_m10^{-8}}{I} \qquad (38)$$

Now

$$I=\left(\frac{4}{T}\int_0^{\frac{T}{4}} i^2dt\right)^{\frac{1}{2}}=\frac{2l}{N}\left(f\int_0^{\frac{1}{4f}} H^2dt\right)^{1/2} \qquad (39)$$

Substituting for H as a function of B we obtain

$$I=\frac{2l}{N}\left(f\int_0^{\frac{1}{4f}}(K_1^2B_m^2\cos^2 wt+2K_1K_2B_m^{11}\times \cos^{11} wt+K_2^2B_m^{20}\cos^{20} wt)dt\right)^{1/2} \qquad (40)$$

Evaluation of this expression (Hudson's manual equation 330)

$$I=\frac{0.707l\,K_1B_m}{N}\left[1+0.942\,\frac{K_2}{K_1}\,B_m^9+0.352\left(\frac{K_2}{K_1}\right)^2B_m^{18}\right]^{1/2} \qquad (41)$$

$$L_{ac}=\frac{N^2A}{K_1l}\,(10^{-8})\left[1+0.942\,\frac{K_2}{K_1}\,B_m^9+0.352\left(\frac{K_2}{K_1}\right)^2B_m^{18}\right]^{-1/2} \qquad (42)$$

By plotting equation 41 for I versus B_m and equation 42 for L_{ac} versus B_m, the plot of L_{ac} versus I is obtained, which is shown in Figure 5.

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A Magnetic-Type Air Circuit Breaker for 15,000-Volt Services

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Synopsis: In the development of the five-kilovolt Magne-Blast air circuit breaker, it was shown that rapidly increased arc resistance was extremely effective in circuit interruption. This paper describes an extension of that principle in the development of a Magne-Blast circuit breaker for 15-kv services with an interrupting rating of 250,000 kva. The higher electrical resistance of the arc necessary for this voltage has been made possible by an increase in the thermal efficiency of the interleaving arc chute. The arc chute which provides this greater efficiency is discussed in some detail. The design of the breaker is described briefly as well as its application to metal-clad switchgear. Test data and representative oscillograms are submitted to substantiate the interrupting rating assigned to this breaker.

MODERN switchgear equipments with moderate-voltage air circuit breakers have been used to considerable advantage during the recent period of industrial expansion. Under the severe service conditions of this greatly accelerated war program, magnetic-type air circuit breakers have proved their worth in reliable operation with a minimum of maintenance. The trend toward air circuit breakers for all moderate distribution voltages has been hastened by this experience, and demands for an extension of the line are real. The design history and performance characteristics of a development that eventually will extend the application of magnetic-type air circuit breakers to 15-kv service with interrupting capacities of 250,000 and 500,000 kva are presented here.

The first Magne-Blast air circuit breakers were introduced for 2,300-to 5,000-volt service. Intended to replace the conventional oil circuit breaker of comparable ratings, these breakers were designed as the removable switching unit of modern vertical-lift metal-clad switchgear. These breakers emphasized the beneficial effects of rapidly increasing the resistance of the arc prior to interruption of a-c circuits, both from the standpoint of current reduction and reduction of the rate of rise of recovery voltage. Heretofore, it had been considered generally that increasing arc resistance only increased the energy that had to be dissipated in the interrupter and was therefore to be avoided.

Since being described before the Institute in 1939,[1] additional sizes had been developed to include 50,000 to 150,000 kva at 5 kv and 250,000 kva at 7.5 kv. The operating record of the several thousand Magne-Blast breakers now in service has been so satisfactory that it was imperative to continue this development into the 15-kv range at 250,000 kva and higher. An extensive development program was therefore initiated to develop a 15,000-volt air circuit breaker. How it has been possible to extend the Magne-Blast principles of interruption in the development of such a 15,000-volt air circuit breaker having 250,000-kva interrupting capacity is described in the following sections. A brief description of the breaker and representative oscillograms also are presented.

Development of the 15-Kv Arc Chute

The primary problems in the development of an arc chute suitable for a 15-kv magnetic air circuit breaker are those of providing sufficient arc cooling to insure positive interruption and sufficiently high dielectric strength to withstand the insulation requirements of this voltage while still maintaining the approximate space requirements of the corresponding oil circuit breaker. The general form of the interleaving arc chute developed for the lower-voltage Magne-Blast breakers provided an interrupter that promised to interrupt considerable current at much higher voltages. This arc chute has parallel fins which project alternately from the two inner sidewalls. As the arc is forced magnetically into the arc chute, it meets fins of gradually increasing

Table I. 15,000-Volt 250,000-Kva Magne-Blast Circuit Breaker

Test Number	Test Voltage	Maximum-Phase Rms Amperes (Closing)	Maximum-Phase Rms Amperes (Initial in Arc Interrupted)	Arcing Time (Cycles)	Trip Impulse to Interruption	Three-Phase Kva Interrupted
Three-Phase Interrupting Tests—Opening Tests						
1–4	14,500		4.6	0.8	2.9	120
5–8	14,500		8	0.8	2.9	200
9–12	14,500		17	1.4	3.6	440
13–22	14,500		38	2.0	4.0	960
23–32	14,500		74	2.1	4.2	1,600
33–36	14,500		130	2.0	4.0	3,300
37–40	14,500		270	2.2	4.3	6,800
41–44	14,500		600	2.2	4.3	15,000
45–46	14,500		1,990	2.1	4.3	50,000
47	14,500		3,000	1.7	3.9	75,000
48	14,500		4,800	1.4	3.8	120,000
49	14,500		10,300	1.3	3.6	260,000
50	14,500		9,300	1.0	3.3	240,000
51	14,500		10,900	1.1	3.3	270,000*
52	10,000		9,700	1.1	3.4	170,000
53	11,000		12,200	0.9	3.2	230,000
54	11,000		15,100	0.9	3.2	290,000
55	14,500		2,200	1.8	4.1	55,000
56	14,500		5,500	1.3	3.6	140,000
57	14,500		10,500	1.0	3.3	260,000
58	14,500		1,710	1.9	4.2	43,000
59	14,500		11,400	1.0	3.1	290,000
Three-Phase Trip-Free Closing–Opening Tests						
60	14,500	2,500	1,670	1.9	4.3	42,000
61	14,500	2,200	1,780	1.8	4.1	45,000
62	14,500	7,700	5,900	1.4	3.4	150,000
63	14,500	12,600	9,200	1.2	3.2	230,000
Three-Phase Two-Operation Duty Cycle–15-Second Reclosing						
64*a*	14,500	12,600	10,200	1.5	3.8	260,000
b	14,500	16,600	12,200	1.2	3.5	310,000**
Three-Phase Interruption Tests–Opening Tests						
65	14,500		8,100	1.3	3.7	200,000
66	14,500		6,700	1.4	3.7	170,000
67	14,500		9,600	1.3	3.5	240,000
68	11,000		17,000	1.3	3.6	320,000
69	11,000		14,200	1.4	3.6	270,000
70	11,000		14,500	1.4	3.7	280,000
71	11,000		11,200	0.9	3.1	210,000

* See Figure 8. ** See Figure 9.

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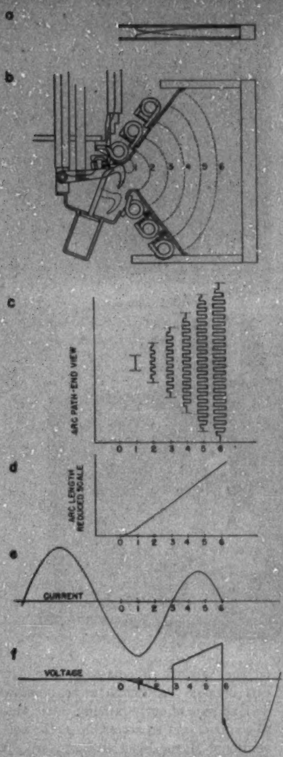


Figure 1. Schematic view of arc interruption in 15,000-volt Magne-Blast arc chute

(a). Plan view of arc chute showing gradual increase in depth of interleaving fins

(b). Successive positions of arc as it is forced into chute. Blowout coils are inserted one at a time as arc travels along arc runners

(c). Cross-section views of successive arc positions

(d). Progressive increase of length of arc—reduced scale

(e). Short-circuit current showing reduction effected by resistance of long arc prior to interruption

(f). Voltage across contacts of breaker which is zero with breaker closed. As contacts part and arc increases in length, its increasing resistance produces a rising arc-voltage drop. At current interruption voltage assumes 60-cycle line-voltage value

depth extending deeper and deeper into the matching slots between the fins of the opposite side. Where the fins interleave across each other, the arc is folded back and forth gradually into a serpentine path as it proceeds into the arc chute, where it is lengthened rapidly, and its resistance is increased greatly through the transfer of thermal energy to the arc-chute surfaces. The effect of the interleaving arc chute in increasing the length and resistance of the arc and the effect of this resistance in reducing the short circuit and instantaneous recovery voltage are shown schematically in Figure 1.

Experience gained in the development of 5,000- and 7,500-volt Magne-Blast breakers showed that the voltage which could be interrupted by an interleaving arc chute is proportional to

(*a*). The length of serpentine arc path.

(*b*). The thermal effectiveness of the interleaving fins in removing heat per unit length of arc.

(*c*). The strength of the transverse magnetic field available for forcing the arc into the chute.

(*d*). The spacing of the arc runners for sustaining recovery voltage.

(*a*). Both the length of the arc path and the thermal efficiency of the interleaving fins in the arc chute are effective in increasing the arc resistance prior to current zero. The short-circuit current is reduced by the introduction of this arc resistance into the circuit, the reduction often being as much as 25 to 30 per cent of current values prior to contact parting. The instantaneous recovery voltage is reduced likewise in magnitude, for the current zero is advanced from its normal lag of 90 degrees toward or beyond the normal voltage zero. For a given value of short-circuit current, this arc resistance must be increased directly with the applied voltage to obtain the same percentage current reduction and instantaneous recovery-voltage reduction.[2,3]

Item *a*, or the relationship between voltage-interrupting characteristics and the length of the serpentine arc path can be checked easily by tests. To do this two 7,500-volt arc chutes and their associated breaker parts were connected in series and opened simultaneously on a 15,000-volt connection in the testing laboratory. These tests demonstrated that two chutes could interrupt the same current as a single chute at double the voltage of a single chute. Cathode-ray oscillographic records illustrated a characteristic of particular interest, for they showed that the recovery voltage immediately following interruption divided equally between the two arc chutes. Within a very short interval this division was modified to values determined by the relative capacitances of the separate arc chutes and contact structures. The resistances of the continuous interleaving hot gas path across each chute immediately following interruption provided an efficient potential divider. It is this same resistance path in a single arc chutes that limits the rate of rise of recovery voltage immediately after current interruption. During a long history of tests on these breakers, this phenomena always has limited effectively the rate of rise of recovery voltage to 300 volts per microsecond or less, regardless of actual circuit characteristics.

In the art of circuit interrupters there are numerous instances where circuit breakers, inherently lacking in this desirable characteristic, have used external resistors to limit the rate of rise of recovery voltage to moderate values.

The length of the serpentine arc path can be increased by providing additional

Figure 2. Interleaving arc chutes for a 15,000-volt 250,000-kva Magne-Blast circuit breaker—one chute opened to show contour of interleaving fins

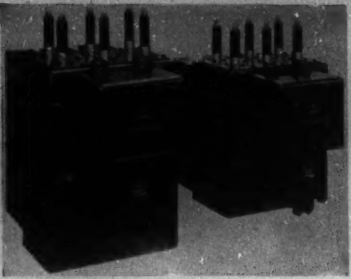


Figure 3 (left). Comparison of 15,000-volt and 7,500-volt Magne-Blast circuit breakers showing similarity of design features

Both breakers rated 250,000-kva interrupting capacity

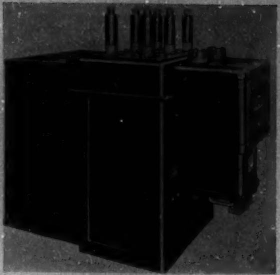
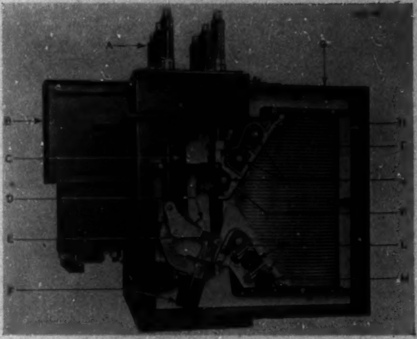


Figure 4

(a—right). 1,200-ampere Magne-Blast air circuit breaker for 15,000-volt service. Rated interrupting capacity 250,000 kva



(b). Cutaway view of circuit breaker through one pole showing arrangement of contacts, arc runners, series blowout coils, one side of interleaving arc chute, and muffler

- *A.* Herkolite bushings
- *B.* Solenoid operating mechanism
- *C.* Operating rod
- *D.* Primary contacts
- *E.* Arcing contacts
- *F.* Piston air booster
- *G.* Removable box barrier
- *H.* Muffler
- *I.* Interleaving arc chute
- *J.* Upper series blowout coils
- *K.* Upper arc runner
- *L.* Lower series blowout coils
- *M.* Lower arc runner

or deeper fins. Increased fin depth is undesirable, as such a design necessarily dictates a wider chute. Here the efficiency of the magnetic system diminishes with the necessary increase in the gap spacing between the magnetic side plates or pole pieces. Additional fins, however, can be added without adding directly to the arc-chute size. Both thinner fins and a reduced spacing between fins add to the arc length directly without increasing the space requirements of the chute.

(*b*). The thermal effectiveness of the fins is increased also by moving the fins closer together, which flattens the normal cylindrical arc. The magnetic forces within the arc itself, however, try to return it to circular cross section which greatly increases the intimacy of the contact between the arc and fin surface. These phenomena are extremely effective in extracting heat from the arc. Figure 2 shows the interleaving arc chutes as developed for a 15-kv Magne-Blast breaker having 250,000-kva interrupting capacity. From this illustration the close proximity of the fins to the narrow interleaving path for the arc may be seen.

(*c*). As the thermal effectiveness of the fins is increased, the arc must be moved at correspondingly greater speeds to prevent excessive fin temperatures. The speed of the arc at any point, however, is proportional to the strength of the magnetic field at that point.

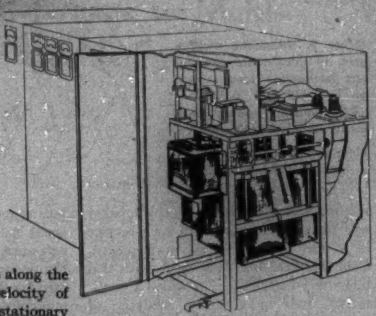
The strength of this magnetic field is a function of the current and the number of turns in the series blowout coils, the cross-sectional area of the iron-pole pieces, and the distance away from the coils. Six blowout coils are used to obtain a magnetic field of maximum flux density over the entire area of the arc chute. These coils are inserted one at a time as the arc travels along the diverging arc runners. Flux measurement tests have shown that, except for very small arc chutes, a much denser more uniform magnetic field can be obtained with several series coils distributed along the entire runner rather than by a single powerful series coil. This is due to the high values of flux leakage unavoidable in such large air-gap circuits, where the effective flux density reduces approximately as the square of the distance away from the coil. Superspeed photographs taken at the rate of 60,000 frames per second of actual arc interruptions also have shown that it is necessary to distribute these blowout coils to obtain uniform arc speed into the chute.

Design Features of the 15-Kv Breaker

During the early stages in the development of this air circuit breaker for 15-kv service, a number of different arrangments of interleaving arc chutes were tested that varied considerably from those used on the original Magne-Blast-type circuit breaker. Although some of these designs were of early promise, none were so compact and so accessible as the conventional Magne-Blast arrangement. It is of interest that a revision of the original form of the Magne-Blast design was completely successful when the lengths of the arc path, thermal efficiency, magnetic field, and arc runner gap were scaled to satisfy the requirements for 15-kv service. Figure 3 illustrates the comparative sizes and design characteristics of one of the earliest (for 7,500-volt service) and the latest (for 15,000-volt service) circuit breakers of this type. All essential features are common to both ratings. The operating mechanism is still in an accessible position in front of the breaker; arc chutes and box barriers extend to the rear, and the entire unit is well adapted for vertical-lift metal-clad equipments. Figure 4*a* shows a 1,200-ampere 15-kv Magne-Blast breaker for 250,000 kva, and Figure 4*b* shows the cutaway cross section through one pole illustrating the arrangement of arc chutes, contacts, blowout coils, and so forth.

When the breaker is tripped the moment an arc is established, it leaves (a—above). Circuit breaker and associated vertical-lift metal-clad switchgear unit

Figure 5



(b—right). Cutaway sketch showing circuit breaker elevated in place in metal-clad compartment

the arcing contacts and travels along the arc runners at an average velocity of 75 feet per second. It is never stationary and moves too rapidly to do any appreciable burning on the surfaces of either the contacts or the runners. Deterioration is reduced so greatly that, even at the maximum interrupting current rating of 14,000 amperes, several hundred circuit interruptions may be completed without requiring maintenance or replacement of arcing surfaces. It was, therefore, desirable to design the contact structure and breaker mechanism with a mechanical life commensurate with this arcing surface life. A new design contact structure has been developed in which both the primary contacts and the arcing contacts pivot on silver-alloy washers. This construction eliminates entirely the need of any flexible braid connections. Fifty thousand mechanical operations made on this type of silver-ring pivot have shown it to be very resistant to wear. This long operating life provides a breaker which is suited just as ideally to industrial applications calling for highly repetitive operations, such as arc-furnace or rolling-mill motor control, as it is to central-station feeder protection where service continuity is the prime requisite.

As the magnetic-field strength available for driving the arc into the arc chute in any series-coil magnetic breaker is directly proportional to the current being interrupted, this magnetic driving force is always inherently weak when interrupting very light currents, particularly transformer magnetizing currents. To maintain the same high arc speeds and short arcing times when interrupting very light currents, as when interrupting heavy current, a supplemental air booster is provided as in the smaller breakers, but it is of increased size. This piston-type air booster is an integral part of the contact structure, and its effectiveness in maintaining short interrupting times during light-current interruptions can be seen in Figure 6, where a maximum of $4^1/_2$ cycles for any value of short-circuit current is shown.

Test Data

Many extended series of interruption tests were made in the development of this breaker to obtain optimum co-ordination between the interleaving arc-path length, fin spacing, magnetic-field strength, and air-booster size. Table I shows a series of interruption tests taken on a production breaker, ranging from transformer magnetizing current values to values well above the interrupting rating of 250,000 kva. In no case did the total breaker operating time from trip impulse to interruption exceed 4.5 cycles. These tests were followed by several trip-free closing operations and several two-unit operation duty-cycle tests. The solenoid mechanism, designed for high closing speeds, practically eliminated arcing on closing operations. Figure 7*a* shows the magnetic oscillogram, and Figure 7*b* shows the cathode-ray oscillogram of a typical interruption test slightly above the 250,000-kva interrupting rating (test 51). The magnetic oscillogram shows the marked reduction in the short-circuit currents of each of the three phases as

Figure 6. Interrupting time versus current of 15,000-volt Magne-Blast circuit breaker—rated 250,000 kva

1. Interrupting time with air boosters
2. Interrupting time without air boosters

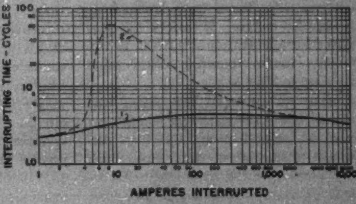
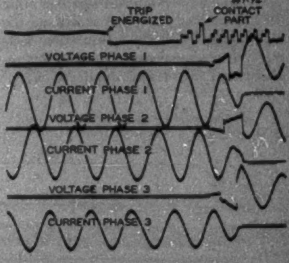
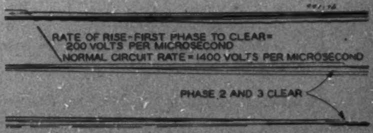


Figure 7



(a). Magnetic oscillogram of three-phase interrupting test at 270,000-kva, 14,500 volts. Test 51 of Table I. Note marked reduction of current due to arc resistance prior to interruption



(b). Cathode-ray oscillogram of recovery voltages across each pole following interruption of three-phase test shown in Figure 7a. Rate of rise is reduced from 1,400 volts per microsecond normal for this circuit to 200 volts per microsecond

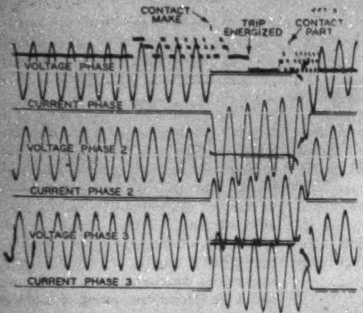


Figure 8. Magnetic oscillogram of three-phase trip-free closing–opening test at 310,000 kva 14,500 volts

Test 64*b*, Table I (second operation of two-unit operation duty cycle—15-second reclosing

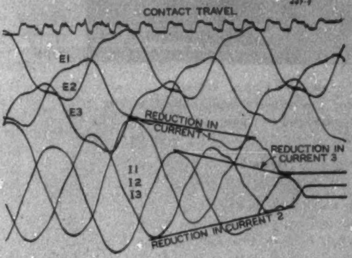


Figure 9. Magnetic oscillogram of three-phase opening test interrupting load current on high side of 6,000-kva 13,200-volt three-phase transformer supplying arc furnace

Note marked reduction of currents prior to interruption

the arc resistance increases. The normal circuit rate of rise of the recovery voltage of the first phase to clear on this circuit is 1,400 volts per microsecond. The resistance of the continuous interleaving hot gas path plus the advancement of the phase position of the current from the crest voltage point reduced the rate of rise of recovery voltage from the normal value of 1,400 volts per microsecond to approximately 200 volts per microsecond.

Figure 9 shows the oscillogram of a typical trip-free closing–opening operation at 310,000 kva. This is the second of a two-unit operation 15-second duty-cycle test 64*b*. These tests demonstrate the ample margin of safety of this breaker for application on 11,000- to 15,000-volt circuits requiring interrupting ratings up to 250,000 kva.

During an extended test series this breaker has demonstrated acceptable factors in reliability and in life. Exhaustive interruption tests have been but a portion of the complete test program. Tests for mechanical and load operations have established its operating life beyond 50,000 operations without the replacement of parts. Insulation tests meet all requirements. Since it was realized that arc-furnace applications represent a severe service duty, this breaker was placed temporarily on such service in a steel mill during its development. Complete field test equipment, with this temporary installation, provided a qualitative and quantitative analysis of breaker performance for such applications. The oscillograms taken during this investigation demonstrated the value of this type of an interrupter in controlling the rate of rise of recovery voltage when interrupting both the load currents and magnetizing currents of an arc-furnace circuit. Figure 9 shows the reduction in current prior to interruption of the breaker interrupting load current of a 13,200-volt 6,000-kva three-phase transformer supplying an arc furnace.

Conclusions

The established principles of the Magne-Blast-type circuit breaker can be extended beyond the range originally contemplated for this type of interrupter, as to both voltage and interrupting ability. The magnetic blasting of an arc into an interleaving chute can be controlled to cool and lengthen this arc at a rate necessary to interrupt the currents of a 250,000-kva rating at 15 kv. The proper co-ordination between arc-chute geometry, magnetic-field distribution and strength, and contact opening speed is used to create arc resistance at a rate most effective in completing efficient circuit interruption. The energy liberated by the arc is absorbed with a minimum of deterioration.

The advantages of the long electrical life of this breaker are combined with a sturdy mechanical design that insures reliability of service with a minimum of care and maintenance.

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Calculation of Unbalanced Voltage Drops in Distribution Circuits With Particular Reference to Multigrounded Neutrals

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PREWAR economic conditions necessitated the use of increasingly precise engineering technique in the design of electric distribution systems. Present wartime demands for conservation of materials have intensified the need for such technique. Control of voltage regulation is a factor of major importance in determining the capital investment in conductors and accessory materials needed for distribution circuits. However, an important hindrance to the application of accurate engineering in taking account of this factor has been the lack of a simple method of calculating voltage drops in circuits having unbalanced conductor impedances and unbalanced loads. The method of symmetrical components, used mainly for calculation of unbalanced fault currents and for solution of a wide variety of problems involving circuit unbalances, has not been accepted generally as a sufficiently rapid method for the solution of voltage-drop problems commonly encountered in distribution-engineering work. Furthermore, simple methods ordinarily used, which neglect unbalanced conditions, often give totally inaccurate and misleading results.

This paper presents the elements of a simplified method applicable particularly to circuits, such as those having multigrounded neutrals, in which phase-to-neutral mutual impedances differ materially from phase-to-phase mutual impedances, but in which the spacings in each of the two groups do not differ greatly from each other. This method provides for the combination of the effects of self- and mutual-impedances, so that calculation of voltage drops is reduced basically to a single vector multiplication of a current and an impedance for each phase conductor, and for the earth–neutral combination treated as a single conductor. The change in circuit voltage produced by these drops then can be determined by vector additions and subtractions.

In practical application the entire process is reduced to simple algebra by use of the familiar method of approximation in which the expression ($IX \sin q+IR \cos q$) is used to represent the arithmetical change in voltage at the load due to drop along the conductors. In this expression all quantities are scalar, I representing load current, X representing conductor reactance, R representing conductor resistance, and q representing the angle between load current and the particular circuit voltage under consideration at the receiving end.

Algebraic working equations may be written for various commonly encountered circuit conditions, using as a basis the vector equations developed in this paper. In combination with working impedance data prepared in accordance with the relations shown in the paper, such algebraic equations may be used for the rapid determination of delivered voltage in distribution circuits involving any type and size of conductor.

Fundamental vector equations are developed in the following, and examples of working algebraic equations and working impedance data are given.

General Equations

With reference to Figure 1, which symbolizes a four-wire three-phase circuit with multigrounded neutral, the voltage drop $\mathbf{V}_A$ in the circuit composed of conductor A and earth return corresponds to the vector difference between source voltage $\mathbf{E}_A$ and load voltage $\mathbf{E}_A'$, as measured to an equivalent earth return plane of reference. The voltage drop $\mathbf{V}_A$ is a quantity not obtainable by direct practical measurement but is employed as a basis for writing expressions for general equations. Equations for $\mathbf{V}_A$ and for the voltage drops in other conductors can be written as follows:

$$\mathbf{V}_A=\mathbf{E}_A-\mathbf{E}_A'=L(\mathbf{I}_A\mathbf{Z}_{AA}+\mathbf{I}_B\mathbf{Z}_{AB}+\mathbf{I}_C\mathbf{Z}_{AC}+\mathbf{I}_N\mathbf{Z}_{AN})\text{ volts} \quad (1)$$

$$\mathbf{V}_B=\mathbf{E}_B-\mathbf{E}_B'=L(\mathbf{I}_A\mathbf{Z}_{AB}+\mathbf{I}_B\mathbf{Z}_{BB}+\mathbf{I}_C\mathbf{Z}_{BC}+\mathbf{I}_N\mathbf{Z}_{BN}) \quad (2)$$

$$\mathbf{V}_C=\mathbf{E}_C-\mathbf{E}_C'=L(\mathbf{I}_A\mathbf{Z}_{AC}+\mathbf{I}_B\mathbf{Z}_{BC}+\mathbf{I}_C\mathbf{Z}_{CC}+\mathbf{I}_N\mathbf{Z}_{CN}) \quad (3)$$

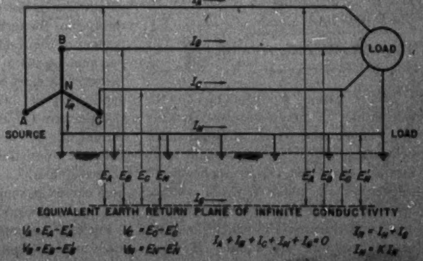

Figure 1. Three-phase four-wire circuit with multigrounded neutral

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$$\mathbf{V}_N=\mathbf{E}_N-\mathbf{E}_N'=L(\mathbf{I}_A\mathbf{Z}_{AN}+\mathbf{I}_B\mathbf{Z}_{BN}+\mathbf{I}_C\mathbf{Z}_{CN}+\mathbf{I}_N\mathbf{Z}_{NN}) \qquad (4)$$

$$\mathbf{I}_A+\mathbf{I}_B+\mathbf{I}_C+\mathbf{I}_N+\mathbf{I}_G=0 \qquad (5)$$

where bold-face characters indicate vector quantities, and

$\mathbf{I}_A$, $\mathbf{I}_B$, and $\mathbf{I}_C$ = currents in conductors A, B, and C, respectively, in amperes

$\mathbf{I}_N$ and $\mathbf{I}_G$ = averaged currents in the neutral conductor and in the earth, respectively, in amperes

$\mathbf{Z}_{AA}$, $\mathbf{Z}_{BB}$, $\mathbf{Z}_{CC}$, and $\mathbf{Z}_{NN}$ = self-impedances of conductors, A, B, C, and N, respectively, with earth return, in ohms per mile

$\mathbf{Z}_{AB}$, $\mathbf{Z}_{AC}$, and so forth = mutual impedances between conductors A and B, and conductors A and C, and so forth, with earth return, respectively, in ohms per mile

$\mathbf{Z}_{AN}$, and so forth = mutual impedance between conductor A and the neutral conductor N, with earth return, and so forth, in ohms per mile

L = length of circuit in miles

Voltage Drop Phase-to-Phase

$$\mathbf{V}_{AB}=\mathbf{V}_A-\mathbf{V}_B=\mathbf{I}_A(\mathbf{Z}_{AA}-\mathbf{Z}_{AB})L-\mathbf{I}_B(\mathbf{Z}_{BB}-\mathbf{Z}_{BA})L+\mathbf{I}_C(\mathbf{Z}_{AC}-\mathbf{Z}_{BC})L+\mathbf{I}_N(\mathbf{Z}_{AN}-\mathbf{Z}_{BN})L \text{ volts} \qquad (6)$$

Equation 6, for calculating phase-to-phase voltage drops under any condition of unbalanced impedances, contains a term with a mutiplier $\mathbf{I}_N$, namely $\mathbf{I}_N\times(\mathbf{Z}_{AN}-\mathbf{Z}_{BN})L$. In this form the equation is not directly usable for a circuit having multigrounded neutral, because the value of $\mathbf{I}_N$ is not readily obtainable. However, if it is assumed that phase-to-neutral spacings are equal, the term in question disappears, because the impedance quantities within the bracket then are equal to each other. The quantities $(\mathbf{Z}_{AA}-\mathbf{Z}_{AB})$, $(\mathbf{Z}_{BB}-\mathbf{Z}_{BA})$, and so forth, represent positive-phase-sequence impedances as published in handbook tables and can be represented more simply by the symbols $\mathbf{Z}_A{}^B$, $\mathbf{Z}_B{}^A$, and so forth, indicating positive-phase-sequence impedance of conductor A at spacing AB, positive-phase-sequence impedance of conductor B at spacing BA, and so forth. Equation 6 may be reduced then to

$$\mathbf{V}_{AB}=\mathbf{I}_A\mathbf{Z}_A{}^BL-\mathbf{I}_B\mathbf{Z}_B{}^AL+\mathbf{I}_C(\mathbf{Z}_C{}^B-\mathbf{Z}_C{}^A)L \text{ volts} \qquad (7)$$

This equation is applicable to the calculation of phase-to-phase voltage drops in circuits having multigrounded neutrals in which phase-to-neutral spacings are equal. It also is applicable to calculation of phase-to-phase voltage drops in circuits having ungrounded or unigrounded neutrals in which phase-to-neutral spacings are equal. If these spacings are unequal, the resulting error in assuming phase-to-neutral spacings equal to the "geometric mean" will be negligible, unless conductors are large in size and phase-to-neutral spacings differ greatly. All equations in this paper for circuits involving multigrounded neutrals are dependent on the foregoing simplifying assumption, since the improvement in accuracy is not deemed to justify the complexity of rigorous calculations involving unequal phase-to-neutral spacings. This is particularly true, since the type of conductor arrangement usually encountered in connection with circuits having multigrounded neutrals is that in which the neutral position is some distance below and centered under the phase conductors. In such arrangements the proportional differences between individual line-to-neutral spacings usually are very small. If phase-to-phase spacings are assumed equal, the last term of equation 7 disappears, and the calculation reduces to a single vector product for each conductor.

Voltage Drop Phase-to-Neutral

$$\mathbf{V}_{AN}=\mathbf{V}_A-\mathbf{V}_N=\mathbf{I}_A(\mathbf{Z}_{AA}-\mathbf{Z}_{AN})L-\mathbf{I}_N(\mathbf{Z}_{NN}-\mathbf{Z}_{AN})L+\mathbf{I}_B(\mathbf{Z}_{AB}-\mathbf{Z}_{BN})L+\mathbf{I}_C(\mathbf{Z}_{AC}-\mathbf{Z}_{CN})L \qquad (8)$$

This may be reduced, if single symbols are used to represent positive-phase-sequence impedance, to

$$\mathbf{V}_{AN}=\mathbf{I}_A\mathbf{Z}_A{}^NL-\mathbf{I}_N\mathbf{Z}_N{}^AL+\mathbf{I}_B(\mathbf{Z}_B{}^N-\mathbf{Z}_B{}^A)L+\mathbf{I}_C(\mathbf{Z}_C{}^N-\mathbf{Z}_C{}^A)L \qquad (9)$$

This equation gives a general expression for voltage drops in multigrounded circuits but is not directly usable in this form, since the term $\mathbf{I}_N$ represents only a portion of the residual load current, and its value has yet to be determined. Furthermore, it must be kept in mind, in the application of formulás for this case, that the term $\mathbf{I}_N$ is defined as the averaged current in the neutral conductor, and that the mutual-impedance terms are defined similarly. These definitions have been chosen to simplify the development of equations suitable for practical use.

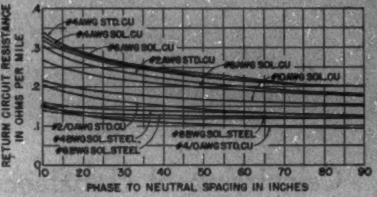


Figure 2. Return-circuit resistance for multigrounded circuits with zero-resistance grounds

Further assumptions also may be made for simplification of equations without much sacrifice in accuracy. These assumptions are:

(*a*). Uniform resistance of grounding electrodes, taken as an average of the resistance of individual grounds.

(*b*). Uniform spacing between grounding electrodes along the line, taken as an average of actual spacings.

(*c*). Uniform resistivity of the earth.

(*d*). Symmetrical arrangement of phase conductors with respect to the neutral conductor. (In cases where this symmetry does not exist, the simplification using geometric mean spacing gives negligible error, unless the three phase-to-neutral spacings are widely different, and conductors are large.)

If these assumptions are used, an equation for $\mathbf{I}_N$ in terms of $\mathbf{I}_R$ and various calculable impedances has been derived from data developed by the Joint Subcommittee on Development and Research, Edison Electric Institute and Bell Telephone System.[1] The derived equation may be written as follows:

$$\mathbf{I}_N=\frac{\mathbf{Z}_{AN}}{\mathbf{Z}_{NN}}\left(\frac{\mathbf{Z}_{NN}-\mathbf{Z}_{AN}u}{\mathbf{Z}_{NN}-\mathbf{Z}_{AN}}\right)\mathbf{I}_R=K\mathbf{I}_R \qquad (10)$$

where

$\mathbf{I}_R=\mathbf{I}_N+\mathbf{I}_G$ = total residual current in amperes, equal to the total return circuit current in the parallel path of the neutral conductor and earth. This current is a fixed vector value for a given condition of loading

$\mathbf{I}_N$ = averaged current in the neutral conductor in amperes (as previously defined) and is also fixed for given circuit and loading conditions

$\mathbf{I}_G$ = averaged current in the earth in amperes and is fixed for given circuit and loading conditions

u = a factor which takes into account the number and average resistance of the grounding electrodes installed along the line and the terminal grounding conditions. Its value is as follows for a circuit with neutral conductor connected at the source end to a station ground of zero ohm resistance and grounded along its length by grounds of finite resistance, the load being connected at the end of the circuit

$$u=1-\left(\frac{\mathbf{Z}_{AN}-\mathbf{Z}_{NN}}{\mathbf{Z}_{AN}}\right)^2\frac{1}{\theta L}\tanh(\theta L)$$

($u=1$ for zero-resistance grounds)

$$\Theta=\sqrt{\frac{Z_{NN}}{R_G}}$$

R_G = averaged resistance of the grounding electrodes divided by the average number of grounds per mile

Z_{AN} = mutual impedance between conductor A and the neutral, with earth return, in ohms per mile

Z_{NN} = self-impedance of the neutral conductor, with earth return, in ohms per mile

L = length of line from source to load in miles

$$K=\frac{Z_{AN}}{Z_{NN}}\left(\frac{Z_{NN}-Z_{AN}u}{Z_{NN}-Z_{AN}}\right)$$

($K=1$ for ungrounded or unigrounded neutral)

If the expression KI_R is substituted in equation 9 for I_N, and if the general superscript NP is used for the line-to-neutral spacing, equation 9 may be written as follows for a multigrounded circuit:

$$V_{AN}=I_AZ_A{}^{NP}L-I_RZ_N{}^{NP}KL+I_B(Z_B{}^{NP}-Z_B{}^{A})L+I_C(Z_C{}^{NP}-Z_C{}^{A})L \text{ volts} \quad (11)$$

This is a general equation for calculating phase-to-neutral voltage drops in any three-phase four-wire multigrounded circuit in which line-to-neutral spacings can be assumed equal, or where these spacings are taken as the geometric mean phase-to-neutral spacings. It applies to lines involving any size or kind of wire for any of the phase or neutral conductors, and to those involving finite resistances of grounding electrodes. It also applies for circuits involving zero-resistance grounds, or ungrounded or unigrounded neutrals, if the values of u and K are determined correctly.

When phase-to-phase spacings are assumed to be equal, in addition to the assumption of equal phase-to-neutral spacings, an expression for phase-to-neutral voltage drops in multigrounded circuits may be developed, as follows:

If the expression

$$\frac{Z_{AN}}{Z_{NN}}\left(\frac{Z_{NN}-Z_{AN}u}{Z_{NN}-Z_{AN}}\right)I_R \text{ is substituted for } I_N$$

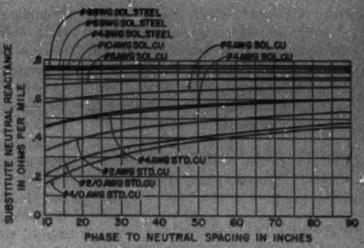
equation 8 may be written

$$V_{AN}=I_A(Z_{AA}-Z_{AN})L-\frac{Z_{AN}}{Z_{NN}}\left(\frac{Z_{NN}-Z_{AN}u}{Z_{NN}-Z_{AN}}\right)(Z_{NN}-Z_{AN})I_RL+I_B(Z_{AB}-Z_{BN})L+I_C(Z_{AC}-Z_{CN})L$$

When the terms are cancelled, this equation may be reduced to

$$V_{AN}=I_A(Z_{AA}-Z_{AN})L-\left(Z_{AN}-\frac{Z_{AN}{}^2u}{Z_{NN}}\right)I_RL+I_B(Z_{AB}-Z_{BN})L+I_C(Z_{AC}-Z_{CN})L \quad (12)$$

Figure 3. Substitute neutral reactance for multigrounded circuits with zero-resistance grounds



Since phase-to-neutral spacings are assumed to be equal, and phase-to-phase spacings also are assumed to be equal, the terms $(Z_{AB}-Z_{BN})$ and $(Z_{AC}-Z_{CN})$ are equal to each other and also equal to $(Z_{AB}-Z_{AN})$.

If this expression is substituted subscripts and superscripts are changed, and $I_BZ_{AA}L$ and $I_CZ_{AA}L$ are added and subtracted, equation 12 becomes

$$V_{AN}=I_AZ_A{}^{N}L-\left(Z_{PN}-\frac{Z_{PN}{}^2u}{Z_{NN}}\right)I_RL+(I_B+I_C)(Z_A{}^{NP}-Z_A{}^{P})L \quad (13)$$

Since

$$I_A+I_B+I_C+I_N+I_G=0$$

and

$$I_N+I_G=I_R$$

$$I_A+I_B+I_C=-I_R$$

and

$$I_B+I_C=-(I_A+I_R)$$

Substituting

$$-(I_A+I_R)$$

for

$$(I_B+I_C)$$

equation 13 reduces to

$$V_{AN}=I_A(Z_A{}^{NP}+Z_A{}^{P}-Z_A{}^{NP})L-I_R\left[\left(Z_{PN}-\frac{Z_{PN}{}^2u}{Z_{NN}}\right)+Z_A{}^{NP}-Z_A{}^{P}\right]L$$

or

$$V_{AN}=I_AZ_A{}^{P}L-I_R\left[\left(Z_{PN}-\frac{Z_{PN}{}^2u}{Z_{NN}}\right)+Z_A{}^{NP}-Z_A{}^{P}\right]L$$

or

$$V_{AN}=I_AZ_A{}^{P}L-I_R\left[\left(Z_{AA}-\frac{Z_{PN}{}^2u}{Z_{NN}}\right)-Z_A{}^{P}\right]L \quad (14)$$

A convenient name for the bracketed expression in the preceding equation is "substitute neutral impedance," and it may be represented by the symbol Z_{SN} as follows:

$$V_{AN}=I_AZ_A{}^{P}L-I_RZ_{SN}L \text{ volts} \quad (15)$$

Similarly

$$V_{BN}=I_BZ_B{}^{P}L-I_RZ_{SN}L$$

and

$$V_{CN}=I_CZ_C{}^{P}L-I_RZ_{SN}L$$

Thus, by using two simplifying assumptions, namely, phase-to-phase spacings are equal, and phase-to-neutral spacings are equal, the calculation of phase-to-neutral voltage drop reduces to a simple vector multiplication of a current and an impedance for the phase conductor and a current and impedance for the return parallel path composed of neutral and earth.

Equation 15 may be used to calculate voltage drops in single-phase and in open-wye circuits in addition to three-phase circuits for which the equation is developed.

Reduction to Algebraic Approximations

As examples of reduction of these equations to algebraic working form by the method of approximation mentioned in the introduction, formulas for line-to-neutral voltage change in a three-

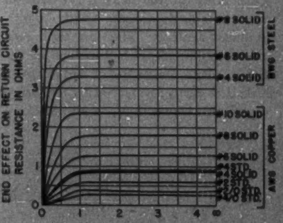


Figure 4. End effect on return-circuit resistance with finite-resistance grounds

$R_G=1$

phase four-wire circuit with multigrounded neutral having a single-phase load connected from line A to neutral, phase sequence being A, B, C, may be written as follows:

Voltage reduction at load, phase A to neutral, is very nearly

$$I_A \cos\phi(R_A+R_R)+I_A \sin\phi(X_A{}^P+X_{SN})$$

Voltage increase, phase B to neutral, is very nearly

$$I_AR_R \cos(60°+\phi)+I_AX_{SN} \sin(60°+\phi)$$

Voltage increase, phase C to neutral, is very nearly

$$I_AR_R \cos(60°-\phi)-I_AX_{SN} \sin(60°-\phi)$$

In these expressions all quantities are scalar and

R_A = total resistance of conductor A
R_R = total return circuit resistance, or resistance of earth–neutral combination
$X_A{}^P$ = total handbook reactance of conductor A at phase-to-phase spacing
X_{SN} = total substitute neutral reactance of the circuit
ϕ = power-factor phase angle of the load current with respect to load voltage

Substitute Neutral Impedance

The bracketed expression for $\mathbf{Z}_{SN}$ in equation 14 can be reduced to the form

$$\mathbf{Z}_{SN}=\mathbf{Z}_{AP}-\frac{\mathbf{Z}_{AN}{}^2}{\mathbf{Z}_{NN}}+\frac{(\mathbf{Z}_{NN}-\mathbf{Z}_{AN})^2}{\mathbf{Z}_{NN}\Theta L}\tanh(\Theta L) \tag{16}$$

In this expression, if resistance of connections to ground is assumed equal to zero, Θ becomes infinite, and the term $(\mathbf{Z}_{NN}-\mathbf{Z}_{AN})^2/(\mathbf{Z}_{NN}\Theta L)\tanh(\Theta L)$ disappears. The first two terms, therefore, represent conditions under which the division of current between neutral and earth is determined only by self- and mutual impedances of phase and neutral conductors and the resistivity of the earth itself. Such conditions exist approximately in the central portions of long untapped circuits with multigrounded neutrals. The last term represents conditions at the load end of the circuit, where the escape of current from neutral to earth is retarded by resistance of ground connections, zero-resistance grounding conditions at the source and being assumed in the derivation of the equation in this particular form. For convenience, the last term of equation 16 is called "end effect." It is greater, the higher the resistance of ground connections. If grounding conditions are no better at the source end of the circuit than along its length, then an additional end effect must be inserted for the source end, doubling the value of the last term. If the load is not located at the end of the circuit, then two end effects in parallel relation must be taken into account, since current will flow in the neutral in both directions from the load.

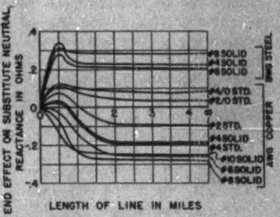


Figure 5. End effect on substitute neutral reactance with finite-resistance grounds

$R_G=1$

It has been customary in calculation of fault currents by the method of symmetrical components to neglect end effects and make calculations under the assumption that ground connections have zero resistance. This often results in negligible error, especially in the case of long circuits, such as transmission lines. However, in the case of voltage-drop calculations in distribution circuits, omission of end effects frequently will result in large error.

For use in practical engineering work, end effects and the other component of $\mathbf{Z}_{SN}$ composed of the first two terms of the expression in equation 16 can be calculated to cover a typical range of conductor types and sizes and incorporated in tabular or chart form, for example, as shown in Figures 2, 3, 4, and 5. Figures 2 and 3 give return circuit resistance and substitute neutral reactance for the first two terms of the expression, respectively, for various conductor types, sizes, and spacings. Figures 4 and 5 give resistance and reactance end effects for various conductors but for a particular value of R_G or resistance of ground connection per mile. A series of such charts for various values of R_G has been prepared as working data for practical application of the method.

In Figures 4 and 5 the last term of equation 16 has been multiplied by L, and total values are read directly opposite circuit lengths. In Figures 2 and 3 the values are unit values per mile and must be multiplied by the length of circuit before substitution in working formulas.

For the construction of the charts of Figures 2, 3, 4, and 5, the following physical constants were used as reasonably typical of conditions likely to be met in practice:

Earth resistivity	50 meter ohms
Distance from conductors to image	2,000 feet
Conductor temperature	65 degrees centigrade
Frequency	60 cycles
Phase-to-phase spacing	40 inches

(Deviations from this value ordinarily encountered have relatively little effect on accuracy)

Steel conductors	high-tensile strength
Current intensity	10 amperes per conductor

Circuits With Ungrounded Neutrals

The foregoing material has been confined to circuits with multigrounded neutrals, but the method is applicable to circuits with ungrounded or unigrounded neutrals by suitable modification of the expression for $\mathbf{Z}_{SN}$. For such circuits this is found to be

$$\mathbf{Z}_{SN}=(\mathbf{Z}_N{}^{NP}+\mathbf{Z}_A{}^{NP}-\mathbf{Z}_A{}^P) \tag{17}$$

These are all positive-phase-sequence impedances readily obtainable from handbook tables.

Reference

1. ENGINEERING REPORTS, volume IV, Joint Subcommittee on Development and Research, Edison Electric Institute, Bell Telephone System. Engineering report 37, COMPUTATION OF ZERO-SEQUENCE IMPEDANCES OF POWER LINES AND CABLES. Page 314, equations 1–3.

Overloading of Transformers—Cases Not Covered by the General Rules

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Synopsis: The general relations covering the heating of transformers and the effects of this heating on safe overloads are simple in principle, but the mathematics involved are complex. When suitable curves are used to express the relations, the simplicity is evident. Loading of transformers and other equipment by temperature is increasingly common and rightly so. An understanding of the principles involved aids in intelligent application of information for loading by temperature. The usefulness of the American Standard *C57.3*, "Guides for Operation of Transformers and Regulators," 1942 would be increased by the inclusion of temperature–time charts so that a solution may be obtained for the unusual loading case. These charts are based on the same principles on which the present rules are based. In addition to simplifying the procedure for taking care of special cases, such charts present a graphic picture of the heating relations of transformers.

THE American Standard "Guides for Operation of Transformers and Regulators" *C57.3*, 1942 and the interim reports on overloading (references 21 and 24) give information which covers many of the overloading conditions encountered with transformers. These rules, however, do not cover every situation. In order to help fill this lack, charts have been prepared which will enable the safety of practically any special case of loading to be determined.

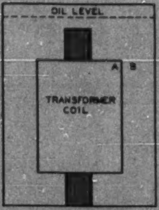

Figure 1. Oil-immersed transformer

In addition to such quantitative values, there is also a need for a qualitative summation of the general principles on which transformer loading is based. Such a summation will aid in correct application of the rules and result in confidence that the application is correct. The general principles are outlined after which their application to specific cases is demonstrated by the use of charts.

General Principles

A familiar operation like heating water on a stove includes all the elements involved in heating and cooling of transformers. When a teakettle of cold water is placed on a stove, the water heats slowly because of its high thermal capacity. The heat given off by the hot gases of the fire must be transmitted to the water, which absorbs heat and increases its temperature. At all times, the temperature of the fire is higher than the temperature of the water, and heat flows from the fire to the water.

After the teakettle is removed from the fire the water remains hot for a considerable time. During this period, its temperature gradually decreases as the water loses heat to the surrounding air.

In a transformer, heat is generated in the copper of the windings and in the iron core. The transformer starting cold, a portion of this heat is absorbed by the copper because of its thermal capacity, and a portion of the heat is transferred to the oil. The oil, in turn, absorbs heat because of its thermal capacity, and, after its temperature increases, dissipates heat to the cooling medium, usually the air surrounding the transformer. In Figure 1 the flow of heat is from the copper at *A* to the oil at *B* and, in turn, from the oil at *B* to the air at *C*. The temperature difference between the copper at *A* and the adjacent oil at *B* is commonly called the copper "gradient." The difference in temperature between the oil at *B* and the air at *C* is commonly called the "oil rise" in temperature. If *A* is the copper adjacent to the hottest oil *B*, the copper temperature of *A* will be the hot-spot copper temperature, and the temperature difference between *A* and *B* will be hot-spot copper gradient. Since the temperature of the hottest spot of the copper is the temperature which controls permissible loading, the charts used in this paper are the charts of hot-spot temperature.

The rate at which insulation deteriorates depends upon the temperature, an increased temperature causing an increased rate of insulation deterioration. It is assumed that continuous temperature of 95 degrees centigrade hot-spot temperature of the copper will result in satisfactory length of life for a transformer. If the operating temperature is below 95 degrees centigrade for part of the time, it can be allowed to be above 95 degrees centigrade for part of the time and still secure the same transformer life. If advantage of this fact is taken, higher loads and higher temperatures can be permitted for short periods of operation. If the transformer is cool when the overload is applied, advantage may be taken of the thermal capacity of the transformer material, and higher overloads with a corresponding higher rate of heat generation can be permitted when the transformer is cool than when it is hot.

Heating and Cooling Curves

Quantitative variations of transformer temperatures with time can be shown conveniently on curves such as Figure 2. The rate of temperature rise with time depends upon the load upon the transformer.

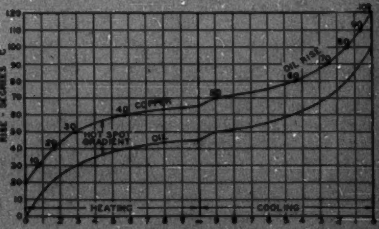

Figure 2. Heating and cooling curves for a typical power transformer carrying full load

Rises shown are hot-spot copper and top oil rise above ambient air temperature

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The curves of Figure 2 have been drawn for 100 per cent load. The left-hand part of the oil curve of Figure 2 shows how the temperature of the oil increases with time as load is applied to the transformer starting cold. The right-hand part of the oil curve shows the manner in which the oil temperature decreases when the starting temperature is higher than the final temperature of the oil. Figure 2 is really two sets of curves in one, the left-hand side of the chart being the curves for increasing copper and oil temperature and the right-hand side of the chart being the curves of decreasing copper and oil temperature. The side of the chart to be used is not determined arbitrarily but is determined by the oil-temperature rise. If the oil rise is above 45 degrees centigrade at the start of the period of time considered, the starting temperature and, consequently, the starting time will fall on the right-hand side of the chart. For example, if the starting oil-temperature rise were 55 degrees centigrade, the starting time would be six hours on the cooling or right-hand side of the chart. If, however, the starting oil-temperature rise were 35 degrees centigrade, the starting time would be four hours on the left-hand side of the chart. In either case, as time elapses the numerical value of the time in hours would increase.

As shown by the left-hand side of Figure 2, when a cold transformer is loaded, the oil temperature rises rapidly almost as a straight line because practically all of the heat generated is absorbed by the copper and iron and oil of the transformer. If no heat-dissipating surface were supplied, the temperature would increase in a straight line indefinitely until failure occurred. After a time the curve of oil-temperature rise departs from a straight line as more heat is dissipated to the surrounding air. After several hours the rate of increase in temperature is very slow, until finally all of the heat generated is being dissipated to the air and the oil temperature no longer increases.

The right-hand side of Figure 2 shows how the oil temperature decreases with time when the ultimate temperature is lower than the initial temperature. The rate of decrease in temperature is most rapid at the beginning near zero time. The rate of decrease in temperature becomes slower with lapse of time, until the same ultimate temperature is reached as for the same load with rising temperature.

Because the copper-over-oil gradient changes rapidly compared to the rate of change of the oil temperature, the copper temperature is assumed to be a constant amount above the oil temperature for any load by the value of the gradient of the transformer, and the copper curve is drawn accordingly. If the oil rise of the transformer is known (as by reading the thermometer on the transformer case) and 100 per cent load is applied, the temperature rise from that time on can be determined from the curve by assuming that the starting time is the time corresponding to the starting oil temperature. If, for example, it is assumed that the starting oil temperature is 30 degrees centigrade, the starting time would be 2.8 hours as read from the oil curve corresponding to 30 degrees centigrade. The transformer does not know whether the 30 degrees centigrade was obtained by carrying 100 per cent load starting cold or by cooling down from a higher temperature. Consequently, it may be assumed for purposes of calculation that the 30-degree-centigrade rise was obtained following the 100 per cent load curve, the load being applied when the transformer was cold. If this load of 100 per cent is carried for two hours, the total time will be 2.8 plus 2 = 4.8 hours. The copper temperature rise at the end of 4.8 hours will be 58 degrees centigrade as read on the copper curve. Each point on the copper curve corresponds to a definite oil temperature on the oil-temperature curve. Accordingly, it is possible to dispense with the oil curve and use only the copper curve if the points on the copper curve are labeled with figures as shown in Figure 2.

Accordingly, for an initial oil temperature of 30 degrees centigrade and 100 per cent load, the starting point on the copper curve is the point marked 30, corresponding to a time of 2.8 hours. Two hours later at a time of 4.8, the copper-temperature rise is read as 58 degrees and the corresponding oil temperature can be estimated as 38 degrees by interpolating between the 30- and 40-degree marks on the copper curve, or by subtracting the known gradient of 20 degrees centigrade. The same general principles apply not only to transformers but also to other types of electrical equipment where thermal characteristics are a factor in determining the safe load which can be carried.

Use of Temperature–Time Chart

Figure 3 consists of curves similar to Figure 2 for 0, 60, 80, 100, 120, 140, 160, 180, and 200 per cent loads for the hot-spot-temperature rise above the ambient air. They were drawn in exactly the same manner as the copper curve of Figure 2. The dotted lines are drawn through points of equal oil temperatures. The oil temperature values are indicated by the figures at each end of the dotted lines.

The purpose of the curves of Figure 3 is not solely to secure a chart by which temperature values can be calculated readily. It has the advantage of presenting the various factors of time, per cent load, and rise in degrees centigrade in such a manner that the physical phenomena may be followed. For example, the curve for 120 per cent load is a curve of the temperature rise of the hot-spot copper above the surrounding air versus time when a transformer is carrying 120 per cent load. The dotted lines merely represent a simple means of indicating the oil temperature on each curve corresponding to the given copper temperature. As stated, the purpose of the chart is not only to determine temperature values for given load cycles but also to present a physical picture of the rise and fall of the temperature during the load cycle.

Example

If the initial oil-temperature rise is 30 degrees centigrade, what will the temperature be at the end of the time

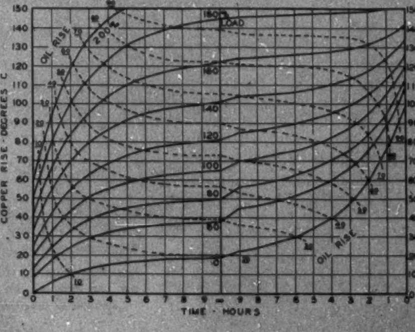


Figure 3. Heating and cooling curves for a typical transformer for various loads for the following full-load values: Hot-spot gradient, 20 degrees centigrade; oil rise, 45 degrees centigrade; ratio of copper loss to iron loss, two; thermal time constant, three hours. Rises shown are hot-spot copper and top oil rise above ambient-air temperature

Table I

Per Cent Load	Time (Hours) Elapsed	Starting	Final	Final Hot-Spot Copper Rise	Final Copper Temperature With 10 Degrees Centigrade Ambient
100	2	2.8	4.8	58	68
140	1	2.2	3.2	82	92
80	1	6.6	7.6	58	68

when 100 per cent load is carried for two hours followed by 140 per cent load for one hour followed by 80 per cent load for one hour? Is this a safe loading condition where the ambient temperature is ten degrees centigrade?

In Figure 3, the time corresponding to 30-degree-centigrade oil rise on the 100 per cent load curve is 2.8 hours; 2.8 hours plus two hours equals 4.8 hours. The copper-temperature rise corresponding to 4.8 hours on the 100 per cent load curve is 58 degrees centigrade. Moving to the 140 per cent load curve, keeping parallel to the dotted lines, one determines the time of 2.2 hours as being the starting time corresponding to the same oil-temperature rise (38 degrees centigrade) as for the finish of the preceding run; 2.2 hours plus one hour equals 3.2 hours. The copper-temperature rise corresponding to 3.2 hours on the 140 per cent load curve is 82 degrees centigrade which is the copper-temperature rise at the end of that time. Again moving parallel to the dotted lines, one finds the starting time for the 80 per cent load curve to be 6.6 hours. Adding 6.6 hours plus one hour equals 7.6 hours. The copper-temperature rise corresponding to 7.6 hours on the 80 per cent load curve is 58 degrees centigrade. By interpolation between the dotted curves, this corresponds to a 44-degree-centigrade oil temperature. For convenience in calculation, these values are arranged in Table I.

Since the hot-spot (copper) temperature is below 95 degrees centigrade at all times, it is obvious that the loads described in Table I may be carried with safety.

Any load cycle can be traced through the curves in similar manner, and the oil and copper temperature at any point on the cycle may be readily determined. In addition, at any point on the cycle the effect of increased time may be determined, since, by inspection, it is obvious whether further increase in time will greatly increase or decrease the rise obtained.

The chart of Figure 3 can, of course, be drawn with other variables plotted on the horizontal and vertical axis, or by means of nomograms or special slide rules. The arrangement of Figure 3, however, produces a better physical picture for most practical cases.

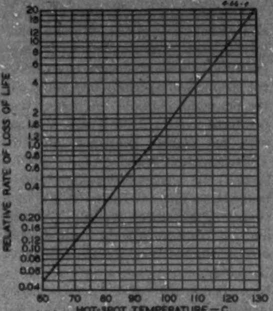


Figure 4. Rate of loss of life of transformer insulation in terms of rate of loss of life at 95 degrees centigrade

The curves of Figure 3 have been drawn for the following transformer characteristics at 100 per cent load: a ratio of copper loss to iron loss of two to one, a hot-spot copper gradient above the oil of 20 degrees centigrade, an ultimate oil rise above the ambient air of 45 degrees centigrade, and a thermal time constant of three hours. Many modern power transformers have characteristics which are approximately the same as these assumed values, and the curves of Figure 3 can be used for such transformers.

Some transformers, particularly older transformers, may have higher gradients and shorter time constants than those used to determine the curves of Figure 3. The chart of Figure 5 has been drawn for use on such transformers with lower overload capabilities, assuming the following full-load values: a hot-spot gradient of 30 degrees centigrade, oil rise 35 degrees centigrade, ratio of copper loss to iron loss at full load 2 to 1, thermal time constant two hours. For general use either the charts of Figure 3 or the chart of Figure 5 will be used, whichever is closer to the characteristics of the actual transformer. In case of doubt Figure 5 should be used.

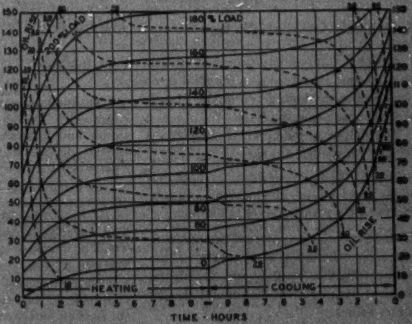
The use of typical charts and the use of typical values of constants for determining permissible overloads of transformers follow the general principles which are used in preparing other guides for operation of transformers, such as American Standard C-57.3. When this section on the guides for operation of transformers is revised, charts such as Figure 3 or Figure 5 or both would add useful information. If other values than those assumed for Figures 3 and 5 are chosen for the other data, these figures can be revised, using the same constants as are used for the other rules. Detailed means by which curves of Figures 2, 3, and 5 may be obtained are described in reference 20 (see the appendix).

For any particular transformer, values of hot-spot copper gradient and top oil rise at full load, and the thermal time constant (the time which would be required for the transformer oil to reach its ultimate temperature if all the heat were absorbed in the oil and no heat were dissipated to the air) may be obtained from the transformer manufacturer. For more accurate results, of course, charts similar to Figures 3 and 5 may be constructed especially for a particular transformer, but this refinement is seldom necessary.

Calculation of Loss of Life of a Transformer

The curves of Figures 3 and 5 can be used to calculate allowable loads and the loss of life for a given loading cycle. The loss of life for a given loading cycle can be calculated from Figure 4 which shows the loss of life in percentage of the loss of life

Figure 5. Heating and cooling curves for a typical transformer for various loads for the following full-load values: Hot-spot gradient of 30 degrees centigrade; oil rise, 35 degrees centigrade; ratio of copper loss to iron loss, two; thermal time constant, two hours. Rises shown are hot-spot copper and top oil rise above ambient-air temperature



of a transformer operated continuously with a hot-spot copper temperature of 95 degrees centigrade. For convenience in calculation it may be assumed that the ultimate temperature is maintained for the total time the load remains constant. (If more precise values are desired, the curve of temperature versus time may be drawn.) The comparative loss of life may be determined as shown below. The average rate of loss of life over the period considered is the rate of aging relative to the base rate of continuous operation at 95 degrees centigrade hot-spot temperature.

EXAMPLE

What is the loss of life during the preceding example as compared to the loss of life at 95 degrees centigrade when the following temperatures were obtained:

68 degrees centigrade at end of two hours of 100 per cent load
92 degrees centigrade at end of one hour of 140 per cent load
68 degrees centigrade at end of one hour of 80 per cent load

What is the highest ambient temperature at which this loading could be carried without the average rate exceeding the normal rate of loss of life?

From Figure 4, the rate of loss of life at 68 degrees centigrade is 0.10; and at 92 degrees centigrade, 0.74 times the rate of loss at 95 degrees centigrade.

The average rate =

$$\frac{0.10\times2+0.74\times1+0.10\times1}{4}=0.26 \text{ times the rate of loss of life at 95 degrees centigrade}$$

This corresponds to an equivalent temperature from Figure 4 of 79 degrees centigrade for the four-hour period. The ambient temperature was ten degrees centigrade; it could have been 95 degrees centigrade − 79 degrees centigrade = 16 degrees higher or equal to 26 degrees centigrade before the average rate of loss of life exceeded the normal rate.

Safe Loads for Short Times

Another approximate method which may be used is to compare the final temperatures with those given in the Standards for short-time loading.

The following temperature limits may be used for recurrent loads:

Two hours or less—110 degrees centigrade
Eight hours or less—105 degrees centigrade
24 hours or less—95 degrees centigrade

Transformer hot-spot copper temperature which is calculated as the sum of the temperature rise from Figure 3, and the ambient temperature should not exceed these values.

If the temperatures obtained during the desired loading do not exceed these values for the times corresponding to the temperature rises on the curve of Figure 3, the loading will be within safe limits. In the preceding example, the copper temperatures given in Table II were obtained with ten degrees centigrade ambient: The temperature limit for 4.8, 3.2, and 7.6 hours is 105 degrees centigrade as shown in the last column of Table II. The final copper temperatures are all below the limit, so consequently the loads may be carried with safety. According to Table II, the ambient temperature could have been equal to 105 − 92 = 13 degrees higher or equal to 23 degrees before safe temperatures were exceeded. This approximate value may be compared with the more accurate value of 26 degrees centigrade obtained by the previous method.

Summation

The general principles underlying safe loading of transformers can be conveniently expressed on a quantitative basis by the use of charts. These charts consider the effect of ambient temperature and thermal capacity of the transformer as well as the other physical constants of the transformer. Thermal characteristics of a transformer cannot be precisely determined, but fortunately precise determination is not necessary for practical use. By using assumed values for constants which give results on the safe side (for example, using a gradient for calculation which is not smaller than the actual gradient, using a thermal time constant which is not larger than the actual time constant) one may be certain that the loading obtained is conservative. This follows the general principles in common use for overloading transformers. The heating of other apparatus follows the same laws as the heating of transformers. Similar charts can be prepared using the same principle for the set of constants which apply to the particular apparatus.

Conclusions

1. The fundamental principles involved in transformer heating and overloading are simple and applicable not only to transformers but to other devices which generate internal heat.

2. By using simple charts, the advisability of a contemplated overload cycle for a transformer can be quickly determined. The usefulness of the American Standards would be increased by the inclusion of such charts.

3. Where the more general rules are not applicable to the loading being considered, the heating and loss of life for the loading can be quickly calculated by the method given.

4. In overloading transformers, the cautions given in the interim report should be observed.

5. If a short-time overload is to be carried on an existing transformer, the starting point in determining the safe time for the load should be the present oil temperature.

Appendix. Description of Curves

The curves of Figures 2, 3, and 5 are curves of heating and cooling versus time and are calculated curves which closely approximate the curves which would be obtained if frequent readings were taken on a transformer in operation, starting with the transformer oil at air temperature for the heating curves and starting with the oil at 100 degrees centigrade rise for the cooling curves. An extensive exposition of the existing relations is given in the references, especially references 2, 5, and 20. The curves are calculated in accordance with these references. To make the curves more generally useful, the effect of thermal capacity of the copper alone is neglected. This produces appreciable error only for short times on the order of 15 minutes or less, and even there the effect is to increase the safety of loading.

Table II

Final Time (Hours)	Final Copper Rise	Final Copper Temperature With 10 Degrees Centigrade Ambient	Temperature Limit
4.8	58	68	105
3.2	82	92	105
7.6	58	68	105

The curves are drawn for the following relations of references 2, 5, and 20:

1. The ultimate hot oil rise over ambient air varies as the 0.8 power of the loss. Consequently, the heat dissipation from the oil to the air varies as the 1.25 power of the temperature.

2. The copper loss varies as the square of the load.

3. The hot-spot copper gradient varies as the 1.6 power of the load.

4. The thermal time constant of the copper alone is assumed to be negligible.

5. The thermal time constant of the whole transformer is assumed to be constant at the given value.

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5. Effect of Overloads on Transformer Life,

The Vibration of Electric Contacts

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IN evaluating an electric switching device, its following characteristics must be considered:

1. Ability to close the circuit without undue deterioration of contacting points.
2. Ability to carry the current without injurious temperature rise.
3. Ability to interrupt the circuit with the least destruction of contacting members.

Considerable attention has been given to the study of the two latter problems, while the "making" of contacts has been practically neglected. However, the engineers interested in the life or in the proper operation of switching devices realize that, in many applications, particularly those involving high initial power surges, the "making" of the contacts may be even more important than their "breaking" performance.

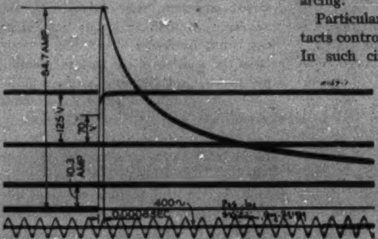

Figure 1. Oscillogram showing current rise in 1,200-watt lamp circuit on 125 volts, direct current

A switch, to "make" properly, must bring the contact points into union without their bouncing or vibrating. That is, once touched, the points should not separate, for otherwise the circuit would be momentarily broken with resulting arcing.

Particularly vulnerable are the contacts controlling tungsten filament lamps. In such circuits, because of the high temperature coefficient of resistivity of the filament material, the initial current is several times that of the normal lamp current. A typical oscillogram, Figure 1, recorded an inrush current of 800 per cent of the normal. Consequently, if vibration of contacts occurs, during their "making" period, the contact points are called upon to interrupt and to "close" a load eight times their rated capacity. Such excessive current, of course, destroys the contacting parts very rapidly.

Vibrating contacts impose a greater duty on the contacting members. Such contacts are subjected to the load interruption, not only during their "breaking" function, but also while they are "making." Naturally, in circuits producing high current surges, this condition causes very rapid disintegration of contact material and welding. The results of vibrating contacts controlling a circuit of lamps are shown in Figure 2. Specimens 2a were closing and opening a circuit having lamps as load and are showing severe pitting and wear of metal at the center portion of the contact; it was caused by "breaking" the circuit after initial "make." Specimens 2b were controlling a d-c noninductive resistance load and are characterized by deterioration of metal at the wings of the contact; this wear was caused by arcing incident to "breaking" of circuit; it will be observed further that the center portion of the contact was not affected because of absence of high current surges in noninductive resistance circuit. Life of contacts vibrating during "making" is reduced, and, in some circuits, their use becomes prohibitive.

Similar destructive results occur in switches controlling motors, since the starting current of motors is several times their "running" values.

In the application of contacts involving sending out a predetermined number of impulses (telephony, electric clock systems, supervisory control, and so forth) for selection of indication or of a function of remotely located apparatus, the contacts must be vibrationless, for otherwise

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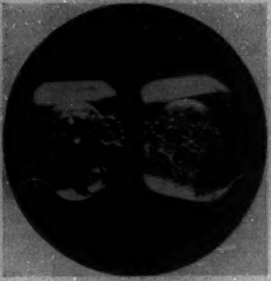
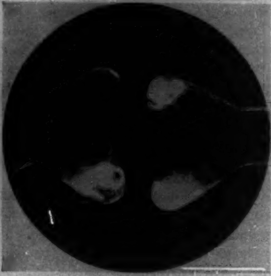


(a). Switch contacts controlling lamp load circuit



(b). Switch contacts controlling noninductive resistance circuit

Figure 2

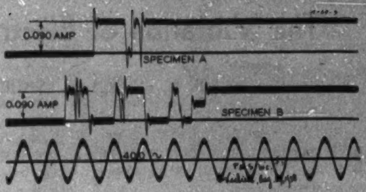
the number of "makes" and "breaks" would not correspond to the assigned code, thus producing erroneous results. While the effect of vibrating contacts may be compensated by the use of slow-acting magnets, such a scheme is not desirable, for it limits the speed of the system.

Functionally the contacts may be divided into two general groups: butting type (contactors, relays, switches, keys, circuit breakers, and so forth) and knife type (knife switches, snap switches, oil switches, circuit breakers, and so forth).

In a switch of elementary design, the blades may produce violent vibration, as indicated in oscillogram Figure 3. Observe several complete circuit interruptions by either specimen. Butting type of contacts, such as used in relays, keys, and so forth, usually close with pronounced bouncing, as shown in Figure 4. Many circuit interruptions took place in relatively lengthy periods of time elapsing between first "make" and final completion of the circuit, in both exposures. Therefore, the matter of vibration suppression becomes an important problem in switching mechanism designs.

Figure 3. Oscillogram showing contact vibration of ordinary snap switch

Snap switches closing noninductive resistance circuit on 125 volts, direct current



For vibrationless operation, it is necessary to prevent contact overtravel at right angles to the direction of movement in case of the knife type of contacts, and to eliminate bouncing in case of butting type. In either case the energy of the moving system in excess of that utilized to unite contacts must be dissipated. Unless this is done, the impact will cause rebound.

For the interruption of d-c circuits, it is generally considered that the contacts must travel with high velocity. However, such a characteristic is objectionable during their "making," for the kinetic energy resulting from this high velocity would be more difficult to absorb.

One of the most common means of contact vibration suppression is to design the stationary contact under spring pressure, so arranged that, when the moving contact strikes the stationary contact, the spring absorbs the energy of the moving system.[1] Similar results may be obtained by the use of a dashpot, which would be put into action on the closing of contacts and thus would act as a a damper in absorbing the energy of the movable contact.

Another method of reducing the contact vibration is to use hollow contacting points,[2] filled with metallic powder. When suddenly arrested by the stationary contact, the powder particles create heat because of friction between them, thus dissipating the energy which would have produced vibration.

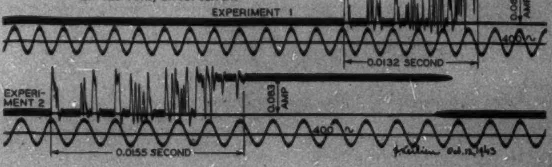
The snap switch offered a particularly difficult problem. Because of its limited size and low cost, elaborate means for the absorption of energy could not be incorporated. Consequently, it was necessary to devise some arrangement to suppress the vibration without external means. The investigation resulted in a contact with self-contained vibration suppressor. This was accomplished by preloading mutually two halves of the flexing contact member. Thus, when the flexing contact arm engaged the stationary member (like a knife switch) the energy, which would have caused vibration, was absorbed by the preloaded contact, without sacrificing high contact velocity.

For the analysis of the factors affecting the "making" of contacts, assume a knife-type snap switch, in which a movable flexing blade engages stationary contacts, completing the circuit. It will be apparent from further discussion that the analysis is applicable to any type of knife contacts. In this switch the movable element (the blade) is brought into rapid motion by a spring which is suddenly released, having been gradually loaded previously by the movement of a manually operable handle. Thus, the operator has no control of the speed with which the contacts engage. The blade collides with the stationary contacts during "making," and this action can be treated as an impact.

Experimental investigations of contact vibration required the study of several pertinent factors. Actual oscillograms, mentioned heretofore, give indications that the oscillations are damped, as expected; the velocity of the moving system is high;[3] the mass of the moving system cannot be materially reduced without the detrimental effects of high current density of current-carrying parts,

Figure 4. Oscillogram showing circuit interruptions during "making" of relay contacts

Relay contacts closing noninductive resistance circuit on 125 volts, direct current



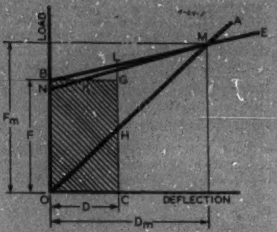


Figure 5. Graphical representation of characteristics of preloaded contact springs

or without making the mechanism undesirably small.

Analysis indicates that there is oblique impact between the blade and the stationary contact which tends to cause a rebound at an angle. It seems quite clear that, without the application of some external force, the blade after impact will separate from the contact, if the blade is a free acting spring. Rebound may be prevented by preloading the blade to the extent necessary to reduce to zero the component of the blade energy perpendicular to its travel.

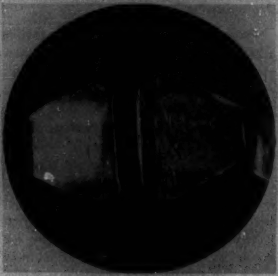


Figure 6. Right switch blade contact after 60,000 closures of circuit containing lamps. Left blade unused

In Figure 5:

D = blade travel after impact within which rebound energy is to be absorbed

F = average force which, acting through distance D, is equivalent to the kinetic energy component perpendicular to the blade travel

D_m = maximum blade deflection

F_m = maximum blade pressure

The work represented by the area *OBGC* is equal to the kinetic energy of the component of rebound perpendicular to the blade travel. This is the amount which must be balanced by the work of deflecting the blade.

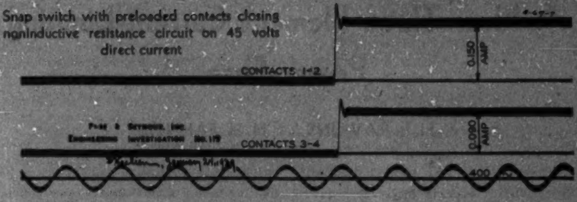


Figure 7. Oscillogram showing vibrationless contact closures of preloaded blade contacts

The load deflection curve *OA* represents a spring blade of suitable characteristics to give adequate contact pressure F_m at maximum deflection D_m, but it is seen that the area, *OHC*, representing the work done in deflecting the spring distance D, is less than *OBGC* and, therefore, insufficient to prevent rebound.

The spring blade, however, which will just prevent rebound, will have a load deflection curve *NM* with *ON* representing the preload. Or a spring having the loading characteristic *BE* will provide a factor of safety.

To check the results experimentally, a number of switches built with the blades of suitable design were tested under most severe practical load (tungsten filament lamps with a special generator capable of producing the high surges needed.)[4] The test specimens were arranged so that the contacts were only "making" under load, while the "breaking" of the circuit was accomplished by another device. There was no visible pitting on any specimen, indicating vibrationless "make," even after 60,000 operations. The appearance of a specimen after test is given in Figure 6, in which the right-hand contact discloses slight grooves resulting from mechanical abrasion of parts. The left-hand blade is an unused sample and is shown for comparison.

Oscillograms further substantiated vibrationless contact (see Figure 7), for the circuit controlled by the contact was not broken after the initial "make." The slight saw-tooth shaped lines at the start of "on" period and at the end of "off" period, as shown on this and previously mentioned oscillograms, represent the record of the galvanometer overtravel, and should not be interpreted as circuit interruptions.

In actual practice the contact is called upon not only to "make" but also to "break" the lamp load circuit. Injuries to contact surfaces resulting from "breaking" contribute to their imperfect "making," ultimately destroying the contacts in spite of the designs, exposing different areas of the contact during "making" and "breaking." The life of switches having preloaded blades has been increased several times as compared with that of a conventional design having no vibration suppression means. This fact has been substantiated by tests of a large number of specimens.

It is quite apparent that designing the contacts so that the energy causing vibration is absorbed at the instant of impact may be readily accomplished by the mutual preloading of the contact blades. Under such conditions the vibration may be eliminated completely, producing a marked increase in the longevity of electric contact.

There is a tendency to increase the load controlled by switch contacts, particularly in the field of lighting. This condition has been created by higher levels of illumination and the impracticability of increasing the number of conductors in existing wiring. Short life may be expected in such cases with switches whose contacts are not suitably designed. Wider application of various contactors, relays, industrial switches, and more stringent requirements in their performance emphasize the necessity of having vibrationless contacts. The described method of vibration suppression, because of its simplicity, could be applied successfully to many switching devices whose vibrationless contact operation is paramount for reliable performance.

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A New Three-Element Current-Limiting Power Fuse

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Synopsis: In order to meet the requirements for the protection of high-voltage motor starters and for protecting small loads and apparatus connected to those parts of electric systems having extremely high short-circuit currents, it has been necessary to increase the current range of the three-element current-limiting fuse. This fuse, which was announced in 1940, has been in commercial use for over two years in the lower ampere ratings.

The development of higher current ratings in a fuse of suitable dimensions produced problems and solutions totally different from those previously encountered. A new type of fusible element has a time-current characteristic particularly suited to current-limiting fuses. Extremely fast melting times on high currents to give correct current-limiting action are combined with a continuous current-carrying capacity and normal melting time of an element having five times the area previously associated with the current-limiting characteristic. The association of this element, with gas-evolving material and quartz sand to give quick and positive current limitation while the metal vapors are absorbed, results in an entirely new fuse construction. The combination of this current-limiting element with a noninductive resistor of special characteristics and a totally enclosed boric-acid "clean-up" fuse has resulted in a current-limiting fuse of more compact construction than has been available heretofore in the ratings involved. Exhaustive tests have demonstrated the suitability of the new fuse for application on high-capacity systems to interrupt the full range of fault currents with minimum system disturbance.

IN order to meet the requirements for a fuse of small size with current-limiting characteristics for connecting light loads and small apparatus to high power systems, a new form of current-limiting fuse was developed and announced in 1940. Three distinct elements characterized this fuse.

1. A current-limiting fusible element which produced a high arc voltage to limit the current.

2. A resistor in parallel with the fusible element to limit the voltage to a predetermined value.

3. A series fuse to interrupt the power follows current through the resistor.

The fuse proved capable of interrupting the highest currents, up to 130,000 amperes at 13,200 volts, without producing voltages in excess of 150 per cent of the peak circuit voltage.

The excellent service record of fuses built with this principle and the inherently higher interrupting ability of current-limiting fuses, coupled with the necessity for limiting the fault current in motor starters and similar apparatus, make it desirable to extend the current range of the current-limiting fuse announced in 1940. In considering the desirable limits in current ratings for current-limiting fuses, the following factors must be considered:

1. The effect of current rating on the current-limiting characteristic. As the current rating of a current-limiting fuse increases, the current to which a fault can be limited also increases, thereby nullifying the beneficial effects of a current-limiting fuse. By reference to Figure 1 it can be seen that the current-limiting effect of a fuse rated above 200 amperes would be appreciable only on systems having high values of short-circuit current.

2. Current-limiting fuses for motor starters are required to have high current ratings with moderate interrupting capacity, whereas current-limiting fuses for station auxiliaries and similar services are required to have high interrupting capacity and relatively low current ratings. The heavier current distribution service can be fulfilled more economically by fuses of lower interrupting rating.

From a consideration of these factors it would seem desirable to extend the new current-limiting fuse to cover a continuous current rating of 200 amperes for voltages through five kilovolts for motor-starter applications, and to cover continuous current ratings of 100 amperes for voltages above five kilovolts to permit tapping small loads from high-capacity systems. Edison Electric Institute and National Electrical Manufacturer's Association have adopted recently the "E" rating for power fuses, which specifies that fuses will carry 100 per cent of rating continuously and melt in exactly five minutes at 200 to 240 per cent of the rating for fuses 100 amperes and below and in exactly ten minutes at 220 to 264 per cent of rating for fuses above 100 amperes. It is seen readily that, for "E"-rated fuses, the proposed ratings cover a wide field of application.

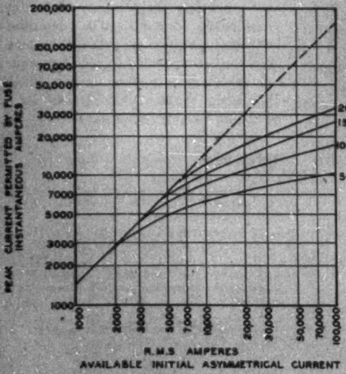

Figure 1 (left). Current-limiting characteristic curves showing maximum peak current permitted by new three-element power fuse for all values of available short-circuit current

Figure 2. Construction of 2.5-kv 200-ampere three-element current-limiting fuse

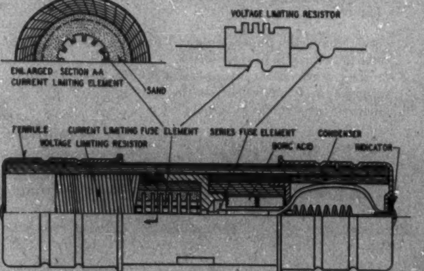

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Figure 3. Five-kilovolt 200-ampere fuse unit in disconnecting-type mounting

In extending the current range of the fuse embodying the new current-limiting principles, it was recognized soon that the same materials and construction as used in the lower ampere ratings could not be used without resulting in a fuse of prohibitive size and cost. The 200-ampere rating could not be obtained in a single fuse of reasonable proportions with proper current-limiting characteristics simply by increasing the number of parallel silver wires in the current-limiting element.

The parallel resistor which diverts current from the current-limiting element required radically different characteristics in order to handle the higher currents in a device of reasonable dimensions. The absorption of metal vapors from a new type of heavier fuse element required a novel construction.

The development of higher current ratings, therefore, produced problems and solutions completely different from those in the lower current ratings.

Construction of the New Fuse

The electrical and physical relationship of the three basic elements in the new fuse are shown in Figure 2, while the overall appearance of a complete fuse with disconnect mounting is shown in Figure 3. As in the current-limiting fuse for lower current ratings announced in 1940, the new power fuse comprises a current-limiting element shunted by a resistance or voltage-limiting element with a series "clean-up" element. All three of these elements are contained in a simple nonrenewable cartridge that is much smaller than existing indoor-type power fuses of lower interrupting capacity and smaller-than-conventional current-limiting fuses of equal rating. The outer casing of heavy Micarta tube with spun-on ferrules contains in one end the assembly, consisting of the current-limiting element and shunt resistor; immediately adjacent is the series-calibrated fuse element in its boric-acid-lined chamber, which on interruption exhausts into the condenser in the other end of the casing.

The heart of the new power fuse is the current-limiting element which is contained in a high-strength fibre tube with threaded ferrules. The conducting portion of the current-limiting element consists of a plurality of specially rolled silver wires supported in a slotted fibre bar. As shown in Figure 4, each silver wire has the appearance of a string of sausages. This construction is obtained by rolling a large diameter wire with reduced sections of approximately one sixth the cross section of the main wire. The restrictions are spaced equally at intervals throughout the length of the conductor. The grooves in the fibre support are approximately twice as deep and slightly wider than the diameter of the silver conductor. The conductor wires are placed in the bottom of the grooves in intimate contact with the fibre, except on the open side of the groove. Fibre washers equally spaced on the slotted fibre bar retain the conductors in the grooves. The space between the fibre support and the fibre casing is filled with a coarse quartz sand of uniform size.

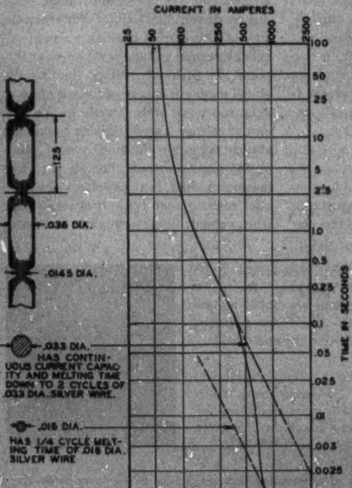


Figure 4 (left). Enlarged view with melting-time–current curve of individual current-limiting fuse element

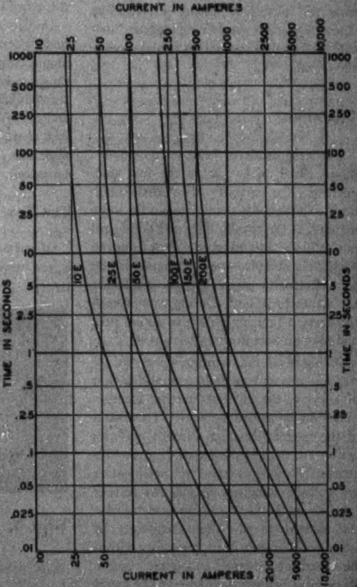


Figure 5 (right). Minimum-melting-time–current characteristics of representative current ratings of three-element power fuse

The shunt resistor or voltage-limiting element which eliminates high-surge voltages incident to current limitation is connected permanently in parallel with the current-limiting element. The resistor consists of two insulated resistance wires wound directly onto the fibre shell of the limiter element and connected on each end to the limiter ferrules. The two layers of resistance wire are wound in opposite directions to minimize inductance. The special resistance wire is essentially nonmagnetic, possesses the virtue of having a moderate positive temperature coefficient of resistance, and has the proper specific resistance for the resistances and sizes needed.

The melting time–current characteristics of the fuse, as shown in Figure 5, are determined by the calibrated element in the boric-acid series fuse. This element is a low-temperature type which consists of a heater and fusible alloy for long melting time characteristics and a reduced fusible section for short melting time characteristics. The series-fuse member is spring withdrawn through the boric-acid-lined interrupting chamber into the condenser to insure fast clearing of low current faults. The condenser consists of several layers of coarse-mesh copper screen which presents the maximum surface to condense the steam evolved from the boric acid during interruption. The action of the operating spring mechanically releases an indicating target outside the ferrule.

Operation

The magnitude of fault current determines which of two ways the fuse shall operate. On currents of overload magnitude the series fuse operates in the same manner as the totally enclosed boric-acid power fuse. The blast of steam from the boric acid deionizes and extinguishes the arc, while the copper screen condenses the steam to water. During this process the current-limiting elements do not fuse and play no part in the interruption.

On short-circuit currents where the series fuse element melts in a small fraction of a cycle, the current-limiting element melts almost simultaneously. The fusion of the current-limiting element and the action of the resultant arcs in the restricted fibre slots tend to produce an arc voltage sufficient to prevent further increase in the fault current and transfer the current to the shunt resistor. In order to eliminate high pressures within the current-limiting chamber, coarse silica sand is used to condense the metal vapors from the fusible elements and cool the fibre gases.

The resistor in parallel with the current-limiting element, being noninductively wound, will stabilize the voltage that occurs during interruption. The maximum voltage that can appeal is simply the *IR* (current-resistance) drop through the resistor. When the instantaneous peak current permitted by the current-limiting element, which can be determined from the curve shown in Figure 1, is known, a value of resistance may be chosen to limit the voltage to any desired figure. It has been determined by repeated tests, however, that all of the current does not transfer instantaneously to the resistor. On high current interruptions approximately 25 per cent of the peak current will continue to flow through the current-limiting element for a short time, resulting in a peak voltage 25 per cent less than the calculated value. In addition to controlling the voltage surges during interruption, the resistor very materially reduces the duty on the current-limiting element. The total energy absorbed by the current-limiting element is less than 15 per cent of the energy that it would absorb without the parallel resistor. This makes possible a very compact current-limiting assembly and is a material factor in the small over-all size of the complete fuse.

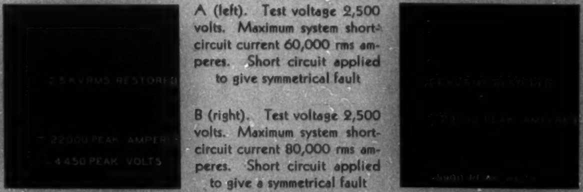
On the high values of short-circuit currents where the series-fuse element melts almost simultaneously with the current-limiting elements, the series fuse arcs throughout the current-limiting action and interrupts the resistor current at the first current zero. Since this current is resistance limited, it is very easily interrupted.

The New Fusible Element

Of particular interest and importance is the new current-limiting fusible element. Although various shapes and sizes of fusible elements have been used to obtain different desired characteristics, there have never been any designs to obtain the peculiar characteristics desired in a current-limiting fuse. The element shown in Figure 4 has all the normal characteristics desired in a fuse for time values in excess of approximately two cycles, and yet for short times of less than one-quarter cycle, it has a very fast melting time. This shift in characteristics results in a fuse that limits the current to 38 per cent of the value that would be obtained if simple straight wires were used. The use of these fusible elements makes possible the cool operation of the high current ratings when carrying full-rated current.

Summary of Tests

The thorough testing of current-limiting fuses involves testing under all degrees of asymmetry as well as on high and low fault currents with various circuit constants, since the severity of duty on the various components of the new current-limiting fuse varies with the point on the generated voltage wave that the current-limiting fuse element melts. A synchronous short-circuiting switch may be used conveniently to obtain these various degrees of asymmetry. The maximum duty on the current-limiting element and the maximum voltage surge due to current limitation occur when fusion of the current-limiting element occurs at normal voltage peak, since at this point the induced voltage due to the stored magnetic energy of the system is at peak value and adds directly to the generator voltage, thereby giving maximum voltage across the limiting element, tending to force current through it. The maximum duty is imposed on the parallel resistor and boric-acid series fuse when current limitation occurs just after



A (left). Test voltage 2,500 volts. Maximum system short-circuit current 60,000 rms amperes. Short circuit applied to give symmetrical fault

B (right). Test voltage 2,500 volts. Maximum system short-circuit current 80,000 rms amperes. Short circuit applied to give a symmetrical fault

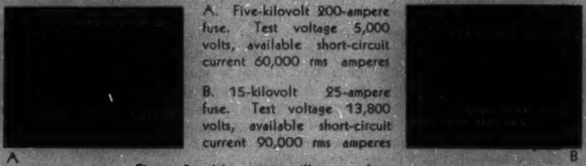
Figure 6. Magnetic oscillograms of interrupting tests on 2.5-kv 200-ampere three-element current-limiting fuse

Note higher peak voltage and longer duration of current resulting in more severe duty on the asymmetrical test

Figure 7. Cathode-ray oscillogram of voltage during interruption by 2.5-kv 200-ampere fuse shown on magnetic oscillogram, Figure 6B

voltage zero, and these two elements carry current until the next voltage zero.

Consider the case of a high-ampere-capacity fuse where an appreciable time is required for melting the current-limiting element. If the fault is initiated at zero voltage, the system voltage will be approaching normal peak value when fusion occurs. Since this condition imposes the most severe duty on the limiter, it follows that an asymmetrical fault represents the most difficult problem of interruption for high-ampere-capacity fuses. On low-current fuses, where fusion of the current-limiting element occurs in a very short time interval, a fault initiated just prior to normal voltage peak or a symmetrical short circuit represents the most severe duty. Therefore, the degree of asymmetry required for the most difficult operation is a variable depending on the current rating of the fuse and the magnitude of the available short-circuit current.



A. Five-kilovolt 200-ampere fuse. Test voltage 5,000 volts, available short-circuit current 60,000 rms amperes

B. 15-kilovolt 25-ampere fuse. Test voltage 13,800 volts, available short-circuit current 90,000 rms amperes

Figure 8. Magnetic oscillograms of interruptions

Short circuit applied to give asymmetrical fault

Consequently, all ratings of fuses have been tested at rated voltage with all conditions of asymmetry on currents of varying magnitudes from minimum melting current up to the interrupting rating of the fuse. Representative magnetic and cathode-ray oscillograms are shown in Figures 6 to 8. The magnetic oscillograms clearly show when current ceases to flow in the current-limiting element, as indicated by the voltage trace changing from a high arc voltage to the smooth sinusoidal form of the generator voltage, and the current trace simultaneously changing from a rapid decline to a sinusoidal form modified by the increasing resistance of the shunt-voltage-limiting element. The cathode-ray oscillograms show that the surge voltage accompanying current limitation does not exceed 160 per cent of normal system voltage on the highest asymmetrical fault currents with high-current fuses. This combination represents the condition under which maximum voltage will be obtained. With any other degree of asymmetry or lower fault current the voltage surge will be less.

A comparison of the magnetic and cathode-ray oscillograms shows that a quick response magnetic oscillograph is entirely adequate for measuring the voltage phenomena of this fuse.

The design of current-limiting fusible elements combined with the low-temperature-type series fuse makes possible the highest desirable current rating in a single fuse unit. On temperature rise tests the 200-ampere fuse carries full rated current without exceeding 30 degrees centigrade rise on the ferrules above a 40-degree-centigrade ambient temperature.

In addition to interrupting tests on the fuse units alone, numerous combination tests have been made with new motor starters throughout the entire current range from overload currents, which the starter interrupts without melting the fuse, to the highest values of short-circuit current, for which the fuse is designed, in which case the fuse interrupts the fault with no damage to the starter. A starter for such combination service must be designed for the particular duties involved.

Conclusions

The current ratings of the three-element current-limiting fuse have been extended by the adoption of new features. Of particular importance is the current-limiting fusible element that gives a time-current characteristic particularly suited for current-limiting fuses. The new features combined in a compact structure permit current ratings ample to protect high-voltage motor starters, other types of apparatus, and small loads connected to high power systems. The new power fuse has the desirable qualities of the lower-current three-element current-limiting fuses in that the voltages during interruption are controlled definitely to eliminate harmful overvoltages.

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Thermal Protection of Transformers Under Overload Conditions

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Synopsis: The overloads recommended in the AIEE transformer subcommittee's report,[1] "Emergency Operation of Transformers," are based on considerably higher transformer hot-spot temperatures for short-time operation than those given in and used to derive the emergency overloads recommended in the American Standard Guides for Operation of Transformers and Regulators *C57.3*.

Overloads may be obtained in close agreement with the recommended emergency overload curves by the use of a relay which is controlled by a specially designed heating coil or by a specially designed relay with a conventional heating coil. Since a relay operates at about the same temperature irrespective of time to avoid too high hot-spot temperature for ultimate conditions, the relay and heating coil must have such characteristics as to permit the transformer hot-spot temperature to reach considerably higher values for short times than for ultimate steady-state conditions. A detailed study of this problem has shown that this may be accomplished satisfactorily by the use of a lagged heating coil which has a relatively large time constant in comparison with that of the transformer winding or by the use of a heating coil, the time constant of which is about the same as that of the winding for operating a relay that is compensated for the changes in ambient temperature.

This relay which has been developed can be set to perform three separate functions: control fans, give a warning signal, and sound an alarm or, if desired, trip the transformer off the circuit. A diagrammatic sketch and photographic prints show the various combinations of bulbs and relays available. It is believed that the relay allows the transformer to be loaded more nearly in agreement with the recommended industry loadings than any previous available device.

THE AIEE transformer subcommittee[1] has recommended higher overloads than those given in the ASA Guides for operation of transformers. These overloads are set up on the assumption that, under rare emergency conditions, it is better to use up a small amount of the expected life of the transformer than to have an interruption of service. Therefore, as a result of the work of the AIEE and the ASA, a demand is developing for a relay that will close its contacts when the limiting load condition is reached.

Since these emergency overloads are based on hot-spot temperatures, the most direct way of safely utilizing the extra capacity would be to furnish a relay with the proper time–temperature characteristics and to operate it from a standard artificial hot-spot heater element. While it may be possible to design such a relay, it does not appear practical, because a relay is normally sensitive to temperature but not to time when it is longer than several minutes; consequently, other means are suggested as follows:

1. To operate a fixed temperature relay from a lagged heating coil (immersed in the cooling fluid),* the time constant of which is many times that of the transformer winding, so that the relay does not reach its operating temperature until the winding temperature and load have risen to the desired value.
2. To operate a relay having an ambient-temperature correction from a heating coil, the time constant of which is the same as that of the transformer winding.

An extensive investigation has shown that, in order to obtain satisfactory results under all conditions, a part or all of the following factors should be taken into consideration:

1. Transformer characteristics—hot-spot rise, top oil rise, oil time constant, and ratio of losses.
2. Time constant of the heating coil immersed in the oil.
3. Temperature rise of heating coil over oil at rated load.
4. Relay operating temperature.
5. Effect of ambient temperature on relay operating temperature.

The purpose of this paper is to show what has been done in meeting the recommended emergency overload curves by taking into account the previously mentioned factors. Finally, calculated overloads obtained with thermal protection on transformers with average characteristics are compared with the rare emergency overloads recommended in the AIEE interim report[1] and the usual emergency overloads recommended in the ASA Guides for operation of transformers, *C*-57.3.

Design of Heating Coil to Meet the Overload Requirements

The use of an ordinary hot-spot indicator and a standard thermostat for overload protection is impractical, because the thermostat, operating relatively quickly at a fixed temperature, could not distinguish between long and short-time intervals and, hence, would operate too quickly in the short-time range, or vice versa.

It is well known that the oil of a transformer has a time lag of several hours due to the total heat-storage capacity of the transformer, whereas, the time lag of winding rise over oil is in the order of 15 to 20 minutes. This means that a relay operated by oil temperature alone would permit excessive winding temperatures under short-time heavy overload conditions, particularly in low ambient temperatures. The scheme of using a thermal relay (immersed in the transformer oil), the temperature rise of which is responsive to load current, has been described[2] and has been in use for several years for thermal protection of transformers.

To obtain satisfactory results with a scheme employing a relay responsive to load current, the temperature rise of the relay (or of a heating coil controlling the relay) must be less than the hot-spot rise over top oil at rated load. Furthermore, the relay must be set to close its contacts at some temperature above that resulting from 100 per cent ultimate conditions in a 30-degree-centigrade ambient temperature. Then on overloads, by the time the temperature of the relay has reached its tripping point, the hot-spot rise will have attained higher values for the higher overloads. It is this difference in the rate of temperature rise that allows the higher hot-spot temperatures for the short-time overloads.

However, if the ambient temperature changes, this scheme does not give correct results in that, if set to allow correct short-time loading at the higher ambient temperatures, it allows too high loadings at the lower ambient temperatures. This deficiency can be corrected by introducing an ambient-temperature

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* The cooling fluid may be mineral oil or any of the noninflammable synthetic liquids (Askarels) developed for transformer use.

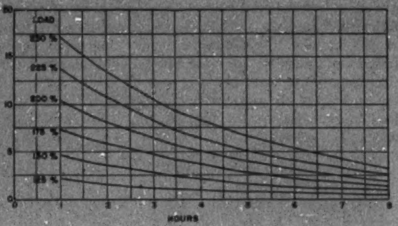

Figure 1. Depression (degrees below ultimate rise) curves for lagged heating coil immersed in oil of transformer under overload conditions following full load

Transformer-oil time constant = 4.0 hours

correction in the mechanism of a relay (mounted on the outside of the transformer), the contacts of which are operated by a bulb enclosed in a heating coil immersed in the top oil and responsive to the load current. On the other hand, if the heating coil is suitably designed and lagged, the ambient-temperature correction can be omitted. By choosing the proper temperature rise of the heating coil (either the lagged or unlagged) and by selecting the right tripping temperature of the relay, a wide flexibility is secured, thereby enabling one to design a protective system to function correctly for almost any condition of loading.

In connection with the use of a lagged heating coil, as long as it was only necessary to meet the ASA emergency overloads, a small amount of lagging was sufficient. However, when the AIEE transformer subcommittee set up the rare emergency overloads based on considerably higher short-time hot-spot temperatures than used in setting up the ASA emergency overloads, it became necessary to modify the design and characteristics of the thermal relay and increase the lagging effect to meet the new conditions. The time constant of the lagged heating coil is now much more than that of the transformer winding.

The problem of calculating the temperature rise of the heating coil (over top oil) to obtain the right time constant under the new condition was found to be very complicated. Tests have shown that, for a transformer winding or a heating coil with no special lagging, the ultimate rise is reached in approximately the same time (15 to 20 minutes), whether the oil temperature is constant or is increasing. However, when a heating coil is lagged, so that it reaches ultimate rise in a given time in oil at a constant temperature, it requires a much longer time to reach its ultimate rise when immersed in oil, the temperature of which is increasing. Theoretically, its rise does not become constant so long as the temperature of the oil is increasing, but it becomes practically constant in eight to ten hours, as illustrated by the "depression curves" (degrees centigrade below ultimate rise), calculated by equation 2 and shown in Figure 1.

The transient temperature rise of this lagged heating coil can not be calculated by an equation of the form usually used[3] for determining the hot-spot rise of a transformer over the oil, that is

$$\theta = \theta_u \left(1 - e^{-\frac{T}{B}}\right) \tag{1}$$

but an entirely different formula* must be used, namely

$$\theta = \theta_u - \left(\frac{FB_c}{B_o - FB_c}\right)\left(\phi_u e^{-\frac{T}{B}}\right) \tag{2}$$

where

θ = temperature rise of heating coil over oil, degrees centigrade

θ_u = ultimate temperature rise of heating coil over oil, degrees centigrade

F = factor allowing for the effect of distributed thermal capacitance of the lagged heating coil

B_c = time constant of heating coil

B_o = time constant of transformer oil

ϕ_u = ultimate temperature of transformer oil above the starting temperature, degrees centigrade

e = 2.718

T = time

Many heat runs ranging in time from one to eight hours' duration were made on a 2,500-kva transformer with heating coils having different lagging, to check equation 2 and to obtain sufficient empirical data on factor F for design purposes.

The fortunate phenomenon, whereby a rising oil temperature gave the heating coil an unexpected long time lag, helped to make it possible to design a thermal protective scheme to meet the wide variety of requirements. In order to take

Table I. Comparison of AIEE and ASA Recommended Overloads With Overloads Permitted by the Protective System

		AIEE Emergency (Times Rated Load)		ASA Emergency (Times Rated Load)	
	Hours	By Relay	Recommended by AIEE*	By Relay	Recommended by ASA
Self-Cooled					
Following full-load 30-degree ambient temperature	24	1.37	1.25	1.15	1.1
	8	1.41	1.35	1.17	1.15
	4	1.52	1.5	1.22	1.2
	2	1.7	1.7	1.30	1.3
	1	1.9	1.9	1.4	1.4
Following full-load zero-degree ambient temperature	24	1.62	1.55	1.42	1.4
	8	1.69	1.65	1.47	1.45
	4	1.84	1.8	1.57	1.5
	2	2.1	2.0	1.75	1.6
	1		2.0	2.0	1.7
Following no-load 30-degree ambient temperature	24	1.37	1.25	1.15	1.10
	8	1.47	1.45	1.3	1.25
	4	1.71	1.7	1.42	1.35
	2	2.05	2.0	1.7	1.50
	1		2.0	2.1	1.60
Following no-load zero-degree ambient temperature	24	1.62	1.55	1.42	1.4
	8	1.75	1.75	1.55	1.55
	4	2.0	2.0	1.75	1.65
	2		2.0	2.15	1.8
	1		2.0	2.6	1.9
Forced-Air-Cooled					
Following full-load 30-degree ambient temperature	24	1.7	1.53	1.52	1.46
	8	1.72	1.66	1.55	1.53
	4	1.8	1.8	1.6	1.6
	2	1.94	1.93	1.73	1.73
	1	2.13	2.13	1.88	1.86
Following full-load zero-degree ambient temperature	24	1.94	1.755	1.8	1.685
	8	1.97	1.885	1.82	1.755
	4	2.07	2.025	1.9	1.825
	2	2.27	2.155	2.1	1.95
	1	2.50	2.355	2.3	2.085

* The maximum load recommended is two times rated load. For forced-air-cooled rating the maximum load recommended is 2.66 times the self-cooled rating.

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* See appendix for derivation.

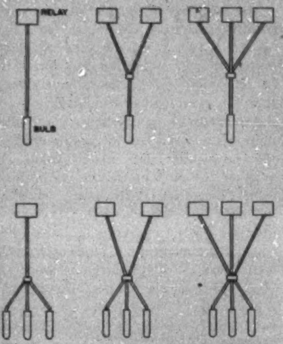

Figure 2. Six combinations of bulbs and relays available for thermal protection of transformers

full advantage of this time lag, however, the temperature rise of the heating coil over oil at rated load (since its time constant is affected by the time constant of the transformer under overload conditions) and the temperature at which it operates the relay must be selected on the basis of the individual transformer characteristics and the kinds of overloads required. Its flexibility enables one to meet almost any requirement.

Factors Affecting and Calculation of Transformer Temperature Rises

Some of the more important factors that determine short-time overloads are the oil time constant and hot-spot rise over top oil. Self-cooled oil time constants ranging from three to five hours were chosen as representative for most transformers. A survey of many recent designs for medium-size power transformers shows that the average time constant is approximately four, extreme limits being as low as three and as high as seven. For the forced-air-cooled oil-immersed ratings (1.33 times the self-cooled rating) the time constant is, of course, less, since the oil rise is much less for a given loss. Two thirds of the self-cooled oil time constant was used in calculating the forced-air-cooled loadings.

As pointed out in the paper by Montsinger and Ketchum,[3] the hot-spot rise over average winding rise in most modern transformers ranges from five to ten degrees centigrade, and for this reason it is believed that a hot-spot rise of 62 degrees centigrade (55+7) represents the average condition. This value is used later.

A formula of the general form of equation 1 should be used to calculate the oil rise before conditions become constant. Equation 2 should be used to calculate the temperature rise of a lagged heating coil over the increasing oil temperature. The temperature rise of the heating coil (at rated load) and the relay-closing temperature should be selected to give the best performance, since these must apply to and satisfy more than one operating condition.

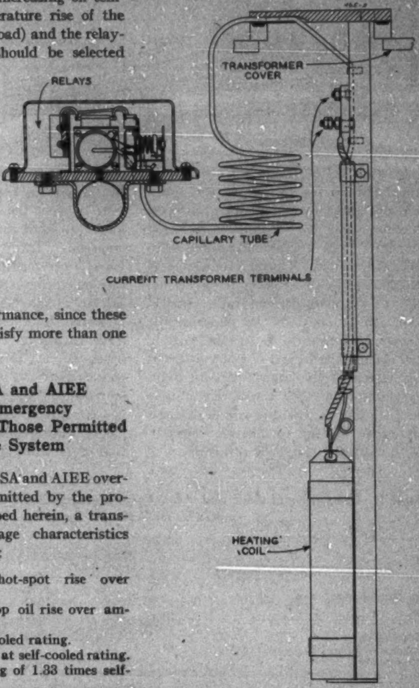

Figure 3. Heating coil and relays

Comparison of ASA and AIEE Recommended Emergency Overloads With Those Permitted by the Protective System

For comparison of ASA and AIEE overloads with those permitted by the protective system described herein, a transformer having average characteristics was chosen, as follows:

62-degree-centigrade hot-spot rise over ambient temperature.
48-degree-centigrade top oil rise over ambient temperature.
3:1 loss ratio at self-cooled rating.
4.0-hour time constant at self-cooled rating.
Forced-air-cooled rating of 1.33 times self-cooled rating.

Table I shows how well the recommended overloads can be followed with the new protective system. For the ASA emergency loads the hot-spot temperatures are below 105,* 110,* 113, 115,* and 120 degrees centigrade for 24, 8, 4, 2, and 1 hours, respectively, in 28 out of 30 conditions shown in Table I. The two cases are: 4 degrees high for 8 hours and 8 degrees high for 24 hours. Both cases follow full load in 30 degrees ambient temperature for forced-air-cooled conditions. No comparison of loads is shown for forced-air-cooled rating following no load, because this is a condition that is not very likely to occur.

For the AIEE emergency loads the hot-spot temperatures are below the recommended values for the short times but above the recommended values in all cases for 24 hours, and in some cases for 8 hours. The principal reason for this is that the temperatures are nearly enough constant in 8 hours' time to produce (with the small difference in loads permitted by the relay for 8 and 24 hours) approximately the same hot-spot temperatures as against a difference of 10 degrees centigrade in the recommended values of 120 degrees centigrade for 8 hours, 110 degrees centigrade for 24 hours. Both the 24-hour loads and hot-spot values may be reduced by insulating a heating coil sufficiently to produce a 24-hour temperature lag, but this is not ordinarily practical.

It seems reasonable to assume that, in most emergencies lasting 24 hours or more, the daily peak load would not be on more than eight hours. Furthermore, as these overloads are intended for rare emergency conditions, once a year or less often (once in 16 years, as estimated in L. W. Clark's paper),[4] the amount of aging at somewhat higher than recommended values for the full 24 hours should be acceptable.

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* Recommended by American Standard C-57.3 section 11.002 (C).

Figure 4. View of bulb, capillary tube, and relays

For transformers having characteristics quite different from the aforementioned, it will be necessary, of course, to use a different tripping temperature for the relay and perhaps a different heating-coil rise at rated load.

Where the load permitted for one or two hours is above the maximum load recommended by AIEE (two times rated load), and it is desirable to limit the maximum load to this value, other means, such as overload current relays, must be arranged to come into action when the maximum desirable load is exceeded. For forced-air cooling two times rated load corresponds to 2.66 times the self-cooled rating.

In general, when the wide range of operating conditions covered is considered, the overloads line up remarkably well with the recommended values, in fact, far better than appeared possible when the problem was attacked first. If the planned conservatism of the ASA and AIEE recommendations is considered, the values given seem to be suitable for operation, but it is a simple matter to design for slightly lower loads throughout if desired.

Good results can be obtained also by using a conventional heating coil with a relay designed to close its contacts at lower temperatures in lower ambients.

Relay

In its simplest form the relay includes a bulb heated by a coil and a metallic bellows which operates a switch. The bulb and the bellows are connected through a capillary tube, and the assembly constitutes a closed system which is filled with a volatile liquid. The pressure of vaporization of the fluid is communicated to the bellows and thence to the switch.

Relay Requirements and Functions

The practical application of a relay and its heating coil to transformers having a wide variety of thermal characteristics and load cycle requirements requires certain variable features:

1. The relay contacts must be capable of being set to close over a temperature range of 90 to 130 degrees centigrade.
2. Means must be provided for selecting different temperature rises of the heating coil at rated load.

If a compromise relay setting and temperature rise of heating coil were used, some transformers would be overloaded excessively before the relay came into action, whereas, others would be loaded far below permissible values.

The new relay combination will perform three separate functions:

1. Control the fans used for supplementary cooling.
2. Give a warning signal when an overload has been on the permissible length of time.
3. Give an alarm or, if preferred, trip the transformer off the circuit when the loads exceed specified limits.

The relay can be supplied for all three of the previously mentioned functions or, if fans are not used, with only the signal and alarm circuits. The contacts are standard switches operated by vapor-tension-type bulb and bellows. The bulbs are located in the heating coil and the bellows and switches conveniently located external to the transformer.

Relay and Bulb Combinations

Six combinations of bulbs and bellows, Figure 2, are available. Figure 3 shows a sketch of the heating coil and connections to relays. Figure 4 shows bulb, capillary tube, and three relays. Figure 5 shows three relays assembled on plate of container.

The combinations shown in Figure 2 will take care of all requirements for either single-phase (with two or three windings) or three-phase transformers, that is, one bulb with one bellows will perform one function (turn on fans) on either a single-phase or three-phase unit. One bulb with three bellows will perform all three functions on a single-phase unit, or if desired it will perform the three functions on a three-phase unit satisfactorily if the load is balanced.

Three bulbs with one bellows will perform either one of the three functions on each phase of a three-phase unit or on each of three windings of a single-phase unit. Three bulbs with three bellows will perform the three functions on a three-phase unit with unbalanced loads or on a single-phase unit with three windings.

When three bulbs are used (with either one or three bellows), one in each phase of a three-phase unit or one in each of three windings of a single-phase unit, if the load is unbalanced the bellows are controlled by the temperature in the phase or winding having the greatest load. This is a new feature used for the first time with bellows, although it has been used successfully for several years in controlling three-element hot-spot temperature indicators for three-phase transformers.

These six combinations, therefore, make it possible to obtain practically ideal protection under expected load conditions.

Figure 5. View of three relays assembled on plate of container

Life Test for Accuracy and Reliability

To determine its accuracy in repeated operations one of the relays was given a life test in which approximately 6,000 operations were performed. The maximum variation was in the order of 0.5 degrees centigrade. These tests indicate that the relay has unquestionable reliability.

Means for Checking the Performance of the Relays in the Field

No matter how reliable a device is, it is sometimes desirable to make calibration tests in the field to be absolutely sure that it is in working order, and to accomplish this the relay is provided with auxiliary means to heat the relay under conditions simulating actual operative temperatures.

Application of Relay to Transformers

Several transformers have been and are now being equipped with these new heating coils and relays. The data obtained on the short-time overloads, made on a self-cooled single-phase transformer (rated *H*-60 cycles, 1,250 kva, 69,000 to 7,500/12,975*Y* volts) equipped with the new protective system, checked the previous tests made to determine the characteristics of the heating coil.

Appendix

Transient Thermal Performance of Transformer-Overload Heating Coil

The transient thermal performance of the heating coil for the transformer overload relay is quite similar to that of a transformer winding, since it is immersed in heated oil and develops an internal temperature rise over oil which must be added to the oil temperature to obtain the temperature of the heating coil. In calculating the transient thermal performance of a transformer winding, it is usually possible to neglect the effect of the rate of change of oil temperature on the temperature rise of the winding above the oil because of the relatively low thermal time constant involved in the transfer of heat from a transformer winding to the oil. For a heating coil having a considerably greater thermal time constant than a normal transformer winding, in calculating the transient thermal performance of the overload relay it is necessary to consider the influence of the rate of change of oil temperature upon the temperature rise of the heating coil over oil. This factor causes the temperature of the heating coil to be somewhat lower during short-time overloads than it would be otherwise and is the primary reason why a heating coil of this nature is so well suited to the control of short-time overloads.

The heating coil includes an assembly containing a loss-producing element and a temperature-sensitive element and having thermal capacitance. This assembly is surrounded by thermal insulation which also has distributed thermal capacitance. It will be assumed that the heating coil can be represented thermally by a suitable value of thermal capacitance and a suitable value of thermal resistance without distributed capacitance. From this assumption it follows that the heating coil will have a thermal time constant which can be determined readily as the product of the thermal capacitance and the thermal resistance. The thermal time constant also can be determined by making a heat run on the heating coil immersed in oil of constant temperature, with constant loss generated in the heating coil, and with the heating coil starting at the temperature of the oil. Under these circumstances the time constant can be determined as the length of time required to reach ultimate heating-coil rise over oil if the initial rate of rise were maintained or as the length of time required to reach 63 0 per cent of ultimate heating-coil rise over oil.

Development of Formula for Calculating the Transient Temperature Rise of a Heating Coil Over Top Oil When the Oil Temperature Is Increasing

KEY TO SYMBOLS

θ = temperature rise in degrees centigrade of heating coil over oil at any time, T

θ_u = ultimate temperature rise in degrees centigrade of heating coil over oil for the overload under consideration = $R \times P$

θ_0 = temperature rise in degrees centigrade of heating coil over oil at start of overload

B_c = time constant of heating coil in minutes = $R \times C$

C = thermal capacity of heating coil in watts—minutes per degree centigrade

R = thermal resistivity of heating coil in degrees centigrade per watt

ϕ_u = ultimate temperature rise in degrees centigrade of transformer oil for the overload under consideration over the temperature at start of overload

B_0 = time constant of transformer oil in minutes

P = watts generated in relay during overload

T = time in minutes

ϵ = 2.718

F = factor allowing for effect of distributed thermal capacity of the heating coil

K = constant of integration

The watts loss generated in the heating coil must equal the sum of the watts absorbed and the watts transferred to the oil. When the oil temperature is rising the watts absorbed by the heating coil consist of two components:

1. Watts required to raise the temperature at the same rate as the oil temperature.
2. Watts absorbed because of its rate of rise over the oil temperature.

These relations can be expressed as follows:

$$P = C\frac{d\theta}{dt} + \frac{C\phi_u}{B_0}\left(\epsilon^{-\frac{T}{B_0}}\right) + \frac{\theta}{R} \qquad (3)$$

If both sides are multiplied by R and θ_u is substituted for RP and B_c for RC

$$B_c\frac{d\theta}{dt} + \theta = \theta_u - \left(\frac{B_c\phi_u}{B_0}\right)\left(\epsilon^{-\frac{T}{B_0}}\right) \qquad (4)$$

Solving for θ

$$\theta = \theta_u + K\left(\epsilon^{-\frac{T}{B_c}}\right) - \left(\frac{B_c}{B_0 - B_c}\right)\phi_u\left(\epsilon^{-\frac{T}{B_0}}\right) \qquad (5)$$

The first term on the right-hand side of equation 5 is the ultimate heating-coil temperature rise over oil which will be realized if the overload is maintained until conditions are constant. The second term is an exponential transient of relatively high decrement. K can be determined by considering conditions at $T = 0$. The third term is a long time transient which represents an amount by which the temperature rise of the heating coil is depressed in rising oil on account of the thermal time constant of the heating coil.

In order to apply equation 5 to a heating coil with distributed thermal capacity, it is necessary to determine a thermal time constant which can be used to represent the performance of the relay. Such a time constant can be approximated closely by measuring the time constant by one of the usual methods previously outlined, and by inserting a correction factor F in equation 5 we obtain

$$\theta = \theta_u + K\left(\epsilon^{-\frac{T}{B_c}}\right) - \left(\frac{FB_c}{B_0 - FB_c}\right)\phi_u\left(\epsilon^{-\frac{T}{B_0}}\right) \qquad (6)$$

Correction factor F can best be determined by comparing test measurements with equation 6.

When $T = 60$ or greater, the term $K\left(\epsilon^{-\frac{T}{B_c}}\right)$ can be neglected.

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The Accuracy of Measurements in Lumped *R*-C Cable Circuits as Used in the Study of Transient Heat Flow

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Synopsis: This paper deals with certain aspects of the accuracy of measurements on a lumped *R-C* cable, and contains definite suggestions as to the desirable number of lumps to be used in such measurements. The effect of capacitive leakage and the influence of sectioning (that is, the influence of the number of lumps) are considered in their relation to the accuracy of measurements of potential and current.

The increasing importance of lumped *R-C* circuits in heat-transfer research is pointed out, and the analogy between the flow of electricity in such circuits and the flow of heat, particularly unsteady-state or transient heat flow, is treated.

The investigations described in this report were carried out at Columbia University on the heat and mass flow analyzer, which is essentially an *R-C* circuit. The results can be used in the appraisal of errors encountered in heat flow and similar problems solved by the analogy method, that is, by lumped *R-C* circuits.

I. Introduction

THE conditions in lumped circuits of this type have been investigated previously.[1] However, these earlier investigations were based mainly on the assumption of ideal capacitors, that is, capacitors which do not leak. Such capacitors are not available. Actual experiments must always be carried out with capacitors which do leak; therefore an investigation of the errors caused by leaking capacitors is appropriate and should prove useful to the electrical engineer, because of the importance of network problems in general.

Lumped *R-C* circuits have gained considerable importance in connection with the study of transient-heat-flow problems. The equations for potential and current distribution in a lumped *R-C* circuit on the terminals of which a voltage is suddenly impressed are identical with those for temperature rise and heat flow in a rod with insulated sides, the ends of which are suddenly exposed to a temperature difference. This analogy is the basis for a method of investigating transient-heat-flow problems, a method which has in recent years found increasing application. In fact, the investigations presented in this paper were prompted by the desire to know more about the accuracy of such a circuit in connection with the application to heat-transfer problems. Although in a number of instances it is possible to compare the results thus obtained with direct thermal measurements or mathematical analysis (see Figure 5 in reference 2, Figures 2 and 4 in reference 6, and Figure 6 in reference 5), in the majority of cases no direct comparison is possible. So far no systematical analysis of the sources of error in this method has been undertaken. This paper presents a first step toward such an analysis.

The application of *R-C* circuits to analyzing unsteady-state or transient-heat-flow problems has been discussed in a previous paper[2] by one of the authors. For the use of such circuits, which are the basis of the analogy method—devised first in Europe by C. L. Beuken[3]—he has developed a tool known as the "heat and mass flow analyzer." The analyzer, made possible largely through grants from the Research Corporation, has been set up at Columbia University, department of mechanical engineering. Since the laboratory was first opened in 1940, it has been used extensively for the investigation of heat-transfer problems too difficult to be solved mathematically. A number of these investigations have been published.[4–9]

Table I

Thermal Unit	Equivalent Electrical Unit
Temperature difference	Voltage
Heat flow	Electric current
Thermal resistivity	Electric resistivity
Thermal capacity	Electric capacitance

The analogy method is based on two principles, namely:

1. The mathematical identity of equations for heat flow and certain electric circuits.

2. The replacing of a circuit with evenly distributed properties by one with lumped properties.

1. The first principle, which is exact, states that the mathematical equations for transient heat flow and for electric resistance–capacitance circuits are identical; hence, by solving an equivalent electrical problem the answer to the thermal problem may be obtained. The solution of the electrical differential equation can be made identical to that of the heat equation by making the proper insertion of equivalent electrical units as in Table I.

2. The second principle, which is an approximation, is that of replacing a differential equation by an equation of finite differences. In the analogy, this means that a body with evenly distributed resistance and capacitance is replaced by a body with "lumped" resistance and "lumped" capacitance. This procedure is common practice in certain fields of electrical engineering.

The study of any thermal problem by means of the analogy method consists of the following steps:

1. Setting up the analogous condition (calculation).

2. Building a resistance–capacitance circuit to represent the heat problem.

3. Subjecting this circuit to the appropriate analogous initial conditions (applying a given voltage at one end of the circuit, which is equivalent to exposure to a given temperature; or applying a current corresponding to a certain heat flow into a body, and so forth).

4. Measuring electrical units—voltage and currents—at points in the circuit analogous to those points at which temperatures and heat flow are to be measured in the body being subjected to heat flow.

5. Converting the results of the electric investigation into heat units (calculation).

A large variety of problems may be solved by the analogy method. The conditions for accuracy are different in each case. The voltage applied, the currents flowing, the duration of the experiment, the length of exposure of various parts of the circuit to higher voltages, even the atmospheric conditions in the laboratory—all influence the results.

For the investigation a particular type of problem was chosen, a type which may be considered as basic to the method in

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general. This problem can be stated as follows: "An *R-C* circuit, initially at zero potential, is suddenly exposed to a potential difference, both ends of the circuit being connected to the supply system by noncapacitive resistances." In thermal terms this problem may be expressed as follows: "A wall, initially at zero temperature, is suddenly exposed on one surface to a finite higher value of temperature, while the ambient facing the other surface is held at the initial temperature. What is the temperature-time history at various points in the wall?" This problem is distinguished by the fact that no part of the circuit remains for long times at elevated potentials, and that the potential at the ends of the circuit remains constant. There are important thermal problems the analogous circuits of which do not show these conditions.

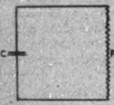


Figure 1. Equivalent circuit of a capacitor

C—Capacitance

R_c—Leakage resistance

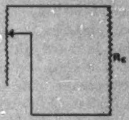


Figure 1a. Equivalent circuit of a capacitor

Capacitance replaced by a variable resistance, to be changed according to voltage change of capacitance

In the following presentation electrical units will be used; reference to equivalent heat units will be added as necessary.

II. Sources of Inaccuracy

In any type of problem to be solved on the analyzer, three sources of inaccuracy occur. They are listed here briefly, and the two which are inherent to the method are discussed individually in parts III and IV, as indicated in the list:

1. Inaccuracy due to leakage and stray currents (part III).
2. Inaccuracy due to lumping (part IV).
3. Inaccuracy in the instruments and in providing the given initial conditions.

The third group of inaccuracies is of course not inherent to this type of circuit. Neither, as far as the thermal aspect is concerned, is it inherent to the analogy method because the same type of errors would occur in measurements made and instruments used in direct thermal measurements. The objective error of the electric instruments used in the analyzer is between 0.3 and 0.5 per cent of the full-scale reading; the proportional error is therefore larger if the instruments are used on the lower part of their scales. In order to safeguard accuracy all measuring instruments in the equipment have several ranges. Thus the instruments can be used largely in the upper part of the scale, where the percentage error is smaller.

In addition to the objective errors there are those subjective ones due to inaccurate reading. The danger from this type of inaccuracy is of course increased by the fact that transients are to be observed, and therefore observations must be made while the needles of the instruments are moving more or less swiftly.

Finally, if changing initial conditions at the surface have to be represented—for instance, change of heat flow or temperature—the manual adjustment of the power supply to this condition may cause additional inaccuracies.

III. Inaccuracy Due to Leakage and Stray Currents

Two causes of inaccuracy have to be considered here: leakage in the capacitors and stray or leakage current in the insulating parts of the circuit. It will be shown later that the two causes can be considered together. But first they will be discussed separately.

1. Capacitive Leakage

The capacitors which are used for representing the thermal capacity draw a leakage current after being loaded and, if left undisturbed, gradually discharge through their leakage resistance. If the capacitors were "ideal," they would draw no leakage current after being charged and would keep their charge indefinitely. In order to determine the inaccuracies introduced by the use of actual capacitors, a series of tests was conducted in which the results obtained from circuits with "ideal" capacitors were compared with the results from circuits with actual capacitors.

It is hard to obtain capacitors with specified leakage, and impossible to obtain ideal capacitors. Therefore, the capacitor, which has an equivalent circuit shown in Figure 1, is replaced by two parallel resistors, shown in the equivalent circuit, Figure 1a. The variable resistor represents the capacitance. At time zero the resistance is zero and gradually increases to infinity when the current is zero. When Figure 1a is considered, it becomes evident that the relative importance of the leakage changes greatly with the state of loading of the capacitor. In the case of the unloaded capacitor the equivalent resistance is very small; compared with this very small resistance the parallel leakage resistance, which is very high, can be neglected. As loading proceeds, the apparent resistance of the capacitor becomes larger, and thus the relative importance of the leakage resistance increases.

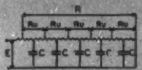


Figure 2. Equivalent circuit of a cable or of a semi-infinite wall

C—Capacitance

R_u—Useful resistance per lump

R—Total useful resistance

E—Over-all voltage

From these considerations it becomes evident that it is sufficient to investigate the steady state, that is, the condition with capacitors loaded to their steady-state potential. If in this case the leakage through the capacitor is within tolerable limits, then it is even more within these limits while the capacitor is at a lower voltage and drawing a heavier current.

Figure 2 shows the type of circuit which is to be investigated. R_u are the "useful" resistors, *C* the capacitors. It

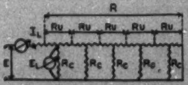


Figure 3. Circuit diagram for investigation of capacitance leakage in circuit Figure 2—steady state

R_c—Resistances equivalent to the leakage of capacitors

R_u—Useful resistance per lump

R—Total resistance

I_L—Current with leaking capacitors

E_L—Voltage with leaking capacitors

E—Over-all voltage

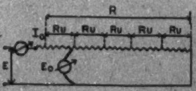


Figure 4. Circuit diagram. Nonleaking capacitors—steady state

E, R, R_u—Same meaning as in Figure 3

E_o—Voltage } with nonleaking "ideal"

I_o—Current } capacitors

is assumed that all sections are equal. This circuit is represented in steady state by the circuit (Figure 3) for actual—leaking—capacitors and by the circuit (Figure 4) for ideal—nonleaking—capacitors.

As mentioned previously, the investigations are carried out by comparing the results from the circuit shown in Figure 3 with those from the circuit shown in Figure 4. Two quantities are of interest in the investigations covered by this paper: voltage and current. On the electric analyzer these represent temperatures and rate of heat flow.

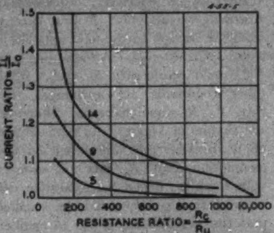


Figure 5. Current ratio, I_L/I_o, plotted as function of the resistance ratio, R_c/R_u. The numbers on the curves (14, 9, 5) indicate the number of sections involved

I_L—Current with leakage (Figure 3)
I_o—Current with no leakage (Figure 4)
R_c—Leakage resistance of capacitance
R_u—Useful resistance per lump

Let the insulation characteristic of the capacitor be expressed by τ. In symbols this would be written as $\tau = R_cC$. τ, which is called the capacitive time constant, has the dimensions of time; thus, if R_c is given in megohms and C in microfarads, τ will be in seconds.

The leakage resistance, R_c, of any capacitor can be expressed by:

$$R_c = \tau/C \text{ megohms} \tag{1}$$

For capacitors of equal quality, the larger the capacitor the smaller will be its leakage resistance.

The product R_uC for each section is the section time constant. Let this time constant be T. Then

$$T = R_uC \tag{2}$$

By comparing equation 1 with equation 2 it can be seen that R_c/R_u and therefore I_L/I_o and E_L/E_o do not depend on the value of C. Thus:

$$\tau/T = R_cC/R_uC = R_c/R_u \tag{3}$$

Let I_L be the current obtained for any given voltage in the circuit of Figure 3 and I_o the current obtained with the same voltage in the circuit of Figure 4. The ratio I_L/I_o of the two currents is plotted in Figure 5. This ratio depends on the relative value of R_c and R_u and on the number of sections. R_c/R_u is shown on the abscissa. Three curves are plotted; these are marked 14, 9, and 5, indicating the number of sections to which the curve refers. The larger the number of sections, the greater will be the current ratio I_L/I_o and the more inaccurate will the method become. As R_c/R_u increases, the ratio I_L/I_o decreases and approaches unity as R_c/R_u approaches infinity.

Example: Let the capacitor time constant $\tau = 50{,}000$ megohms × μf ($= 50{,}000$ seconds). Let each section have a resistance R_u of 0.9 megohm and a capacitance of 55.5μf. Section time constant T is $T = 0.9 \times 55.5 =$ 50 megohms × $\mu f = 50$ seconds $\therefore \tau/T =$ $50{,}000/50 = 1{,}000$. The current ratio I_L/I_o for 14 sections is then 1.055 (from Figure 5).

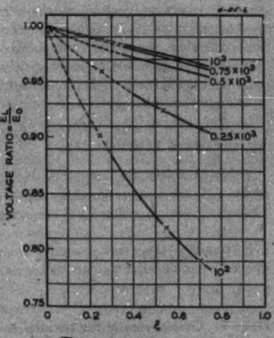


Figure 6. Voltage ratio, E_L/E_o, plotted against fractional resistance

($\xi = 0.5$ is, for example, the center of the resistance.) 14 sections. The figures on the curves (10^3, 0.5×10^3, and so forth) indicate the resistance ratio, R_c/R_u. The xs show points taken with nonuniform subdivision

In Figure 6 the voltages, E_L and E_o of the same circuits are compared. The ratio of these voltages is different at various distances from the beginning of the circuits. On the abscissa the fraction of the total resistance R is plotted (see Figure 2). For example, $\xi = 0.5$ indicates the center of the total resistance. On the ordinate the voltage ratio, E_L/E_o is plotted. E_L/E_o is the ratio of the voltage obtained at any one point from circuit Figure 4 to that obtained at the same point from circuit Figure 3. The total number of sections was 14. The various curves refer to different resistance ratios R_c/R_u.

Figures 7 and 8 are similar to Figure 6 with the difference that in Figure 7 the total number of sections was nine and, in Figure 8, the total number of sections was five.

The same results are obtained in any experiments of this type (with ideal equipment) as long as T remains unchanged (see equation 2). The same results are obtained with large capacitors and small resistors as with small capacitors and large resistors. Equation 3 shows that the introduction of capacitor leakage does not modify this statement. Even with leaking capacitors the same accuracy or, of course, the same error, is reached with any value of T, independent of the distribution of T between resistance and capacitance. From this viewpoint, it becomes obvious that it is desirable to work with as few sections as possible, as may also be found qualitatively from consideration of the circuit of Figure 3.

It may be noted here that the requirements resulting from the investigations of part IV are in the opposite direction, calling for many sections.

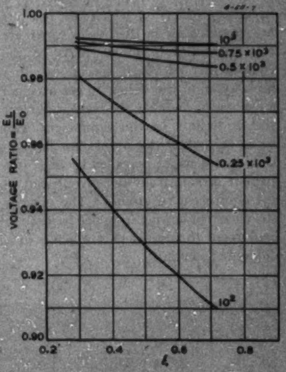


Figure 7. Voltage ratio, E_L/E_o, against fractional resistance

(For example, $\xi = 0.5$ is the center of the resistance.) Nine sections. The figures on the curves (10^3, 0.5×10^3, and so forth) indicate the resistance ratio, R_c/R_u

2. Equipment Leakage (Other Than Capacitive Leakage)

So far it has been assumed that the equipment other than capacitors is free from leakage. Figure 9 shows part of the

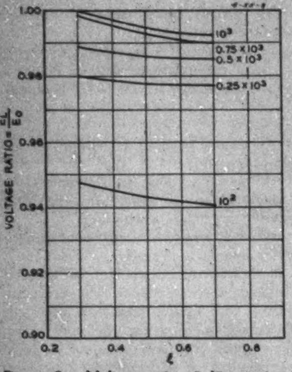


Figure 8. Voltage ratio, E_L/E_0, against fractional resistance

(For example, $\xi=0.5$ is the center of the resistance.) Five sections. The figures on the curves (10^3, 0.5×10^3, and so forth) indicate the resistance ratio, R_c/R_u

laboratory equipment. It is obvious that many leaks do occur—for example, in the insulating panels on which the resistors are mounted, or in the busses which are used to connect various parts of the equipment. Although special care has been taken in the design to get as small a leakage as possible, the leakage current still is finite. In this equipment, two types of undesired currents are flowing. One, which might be called leakage, is a current flowing from any given point in the equipment to ground. The other is stray current flowing from one part of the equipment to another part which is at a different potential.

The leakage current in the insulating part may be thought of as an additional current in the capacitor. The equipment leakage (not capacitor leakage) in each section may be thought of as being replaced by one leakage resistor R_B which is parallel to the leakage resistor R_c of the capacitor. The equivalent total leakage resistance per section R_L is to be found from $1/R_L=1/R_c+1/R_B$ (see Figure 10).

The value of R_B changes with temperature and atmospheric conditions. In dry conditions, and, with all sections connected, it amounts on the Columbia analyzer to approximately 150 megohms. R_B is of course independent of the capacitance and also of the time constant T of the section. Therefore equation 3 does not hold if equipment leakage is to be considered. It makes considerable difference how T—R_uC—is divided between capacitance C and resistance R_u. The influence of R_B on R_L is smaller if R_c is smaller; moreover small R_c means, with unchanged value of T, smaller values of R_u. Therefore, from the standpoint of leakage, it is desirable to work with as high a capacitance, and consequently as low a resistance, as possible.

Example: Let $T=60$; $\tau=50{,}000$; $R_B=$ 3,000 megohms; 14 sections.

Table II shows the values of R_L/R_u and of I_L/I_0 for various possible combinations of R_u and C which yield the same $T(=60)$.

Fortunately the stray currents from one section of the circuit to another are so small that they can be neglected here. If they would be larger, the following reasoning would apply: A stray current from one section to the next acts like a shunt for resistor R_u. A total resistance R_u' would result with $R_u'<R_u$. If all other conditions are unchanged, the ratio R_u'/R_c would increase. However, the circuits of Figures 5 to 8 would not apply, because they are based on the assumption that R_u has the correct value, while under consideration of stray currents R_u' would be smaller than the correct value of R_u. A number of graphs would have to be determined, each holding for one ratio of R_u/R_u'.

Figure 9. General view of the electrical apparatus used in solving problems in three-dimensional heat flow by electrical analogy

Power photo

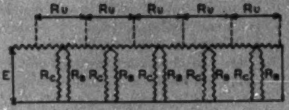


Figure 10. Equivalent-circuit leaking capacitors and leaking boards

R_u—Useful resistance
R_c—Leakage resistance of capacitors
R_B—Leakage resistance of board
E—Over-all voltage

So far it has been assumed that all sections are of equal resistance. In the thermal problem this would mean that all sections are of equal thickness. It is of course possible to make nonuniform lumps. If, for example, a circuit with a total resistance of 1.2 megohms (equivalent perhaps to a wall 12 inches thick) has to be investigated, and 12 sections or lumps are available, it is possible either to make 12 lumps, each with a resistance of 0.12 megohm (one inch thick), or to make the first three or four lumps with a resistance of only 0.06 megohm each (one half inch thick) and increase the resistance (thickness) of the others accordingly, so as to again cover the entire resistance (thickness) of 1.2 megohms (12 inches). Any other distribution would also be possible. One case was investigated: that of three small sections at the beginning of the circuit and ten large sections at the end of the circuit. Each of the three small sections at the beginning of the circuit has one third of

Table II

C	R_u	R_c	R_L	R_L/R_u	I_L/I_0
150	0.40	333	300	750	1.083
120	0.50	416	365	730	1.086
90	0.667	555	470	705	1.090
60	1.0	833	652	652	1.098
30	2.0	1,667	1,075	538	1.122
15	4.0	3,333	1,580	395	1.160

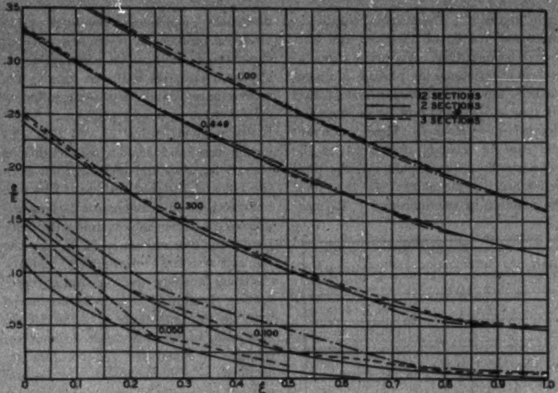


Figure 11. Voltage (temperature) rise in the circuit (wall)

Relative film resistance $m_h=2$, $m_c=1$. The figures on the curves indicate the dimensionless time, t/RC_t

the resistance and one third of the capacitance of the later sections. (The one-third resistance and capacitance correspond to one-third section thickness.) The results were compared with those of a uniform circuit of the same time constant. Two cases were investigated: one for $R_c/R_u=100$, the other for $R_c/R_u=250$. (R_c/R_u referring in both cases to one lump in the uniformly subdivided circuit.) The ratios I_L/I_s for evenly distributed sections were 1.49 ($R_c/R_u=100$) and 1.22 ($R_c/R_u=250$), respectively; the corresponding values for uneven lumps or sections were 1.48 and 1.21, respectively. The measured points for the temperatures are indicated in Figure 6 (by x).

All the circuits shown so far have been based on the assumption of zero film resistance on both the hot and cold surfaces. In this connection a word of explanation is necessary. The heat exchange between a hot surface and the cooler ambient is generally characterized by a "film conductance" value (expressed, for example, in Btu per hour, square foot, degrees Fahrenheit). The underlying concept is that the heat exchange, following Newton's law, is proportional to the temperature difference between the surface and the surrounding. Although in many instances this proportionality does not exist, the introduction of a film conductance value is helpful. In electrical terms, film conductance is represented by a noncapacitive resistance at the end of the circuit. Circuits having zero film resistances were chosen because they illustrate the worst possible conditions for accuracy. If the film resistances are not zero, then additional resistors have to be added: one on the "hot end" of the circuit, representing the film resistance of the hot surface, and one on the "cold end" of the circuit, representing the film resistance of the cold surface. If such additional resistances were applied, the voltage drop in that part of the circuit which represents the wall would be only a part of the total voltage drop. As a result, the ratio of the current with capacitors to that without capacitors would become smaller.

IV. Inaccuracy Due to Lumping

1. Concept

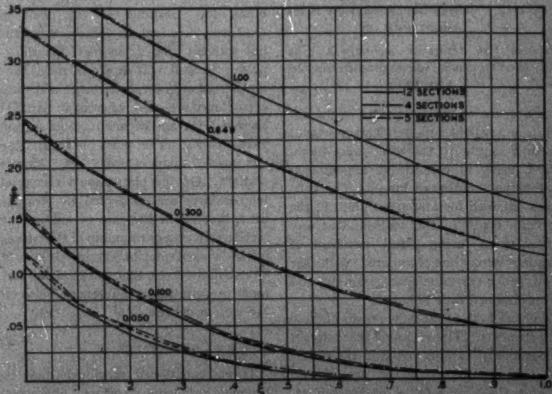
One could conceive as a theoretical limit of an equipment without any leakage, either in the capacitors or in the panels. None of the errors discussed in part III would occur, and yet the equipment would yield only approximate answers because of the errors due to lumping.

The selection of a finite number of lumps or sections instead of an infinite number is, mathematically speaking, equivalent to replacing a differential equation by an equation of finite differences. The main points of interest in this connection are the voltages, corresponding to the temperatures during the gradual heating up. In steady state the voltage–space distribution is theoretically a straight line; the actually measured points all fall on this line, independent of the number of sections used. During the heating-up period, however, more or less important discrepancies between the theoretical result and that obtained with only a few sections will occur.

In order to investigate the influence of the number of sections on the accuracy—from the viewpoint of "lumping"—it is necessary to know the theoretical curves of temperature rise. Such curves are not yet available; therefore, the curves obtained from tests in which 12 sections

Figure 12. Voltage (temperature) rise in the circuit (wall)

Relative film resistance $m_h=2$, $m_c=1$. The figures on the curves indicate the dimensionless time, t/RC_t



were used were considered to be sufficiently close to the theoretical value. This assumption is upheld by past experiments and also by the fact that no measured differences were observed between the curves taken with 15 and those with 12 lumps.

2. PROCEDURE

The procedure for determining the influence of sectioning was as follows: the voltage rise at various points in the circuit—equivalent to a temperature rise at various depths in the wall—were measured, using in consecutive experiments a different number of lumps. The voltages obtained for any given point in the circuit—depth in the wall—and at any given time, using small numbers of lumps, were then compared with the voltage for the same space, position, and time obtained with 12 lumps.

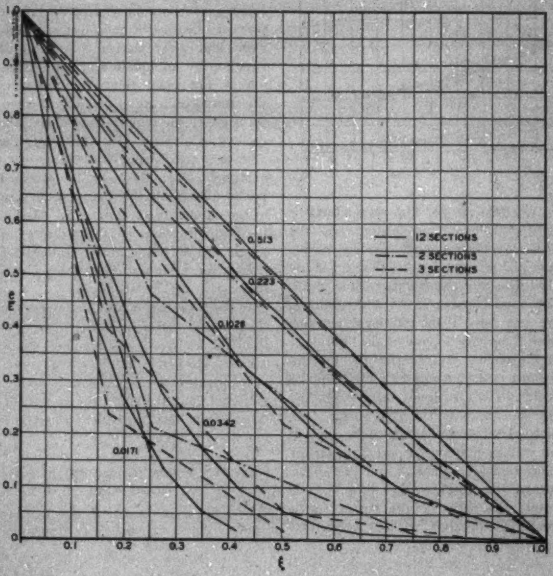


Figure 13. Voltage (temperature) rise in the circuit (wall)

Relative film resistance $m_h=m_c=0$. The figures on the curves indicate the dimensionless time, t/RC_t

Figures 11 to 14 show the results of these experiments. The ratios of the voltages measured at various points in the circuit to the impressed voltage are plotted against the fraction of the total resistance, as in Figure 6. The thermal parallel is, of course, fractional thickness and fractional temperature. Values of time as well as of resistance are plotted in dimensionless units. The dimensionless unit for time is t/RC_t and that for resistance or depth is $\xi=r/R=x/L$. Here t indicates the time in seconds after start of the experiment; R (as in part III) the total resistance in megohms; C_t the total capacitance in microfarads; r, in megohms, the resistance at the point in question; x, in feet, its distance from the hot surface; and L, in feet, the thickness of the wall. (It should be noted that on these charts, total resistance and total capacitance are used, whereas in part III the resistance and capacitance per section were used.) The lines between the measured temperatures are drawn straight. On the plots shown measured temperatures are connected by straight lines, because in the case of only a few sections the proper rounding of the curves might be somewhat uncertain. It should be noted, however, that the straight lines tend to accentuate differences between the various curves.

3. FILM CONDUCTANCE

It was mentioned previously that in thermal problems "surface film conductances," which are represented in the electrical problem by resistances without capacitance, have to be introduced. It appeared probable that the surface film conductance would have some influence on the errors caused by lumping. A preliminary investigation proved that this is the case and that worst conditions prevail for zero film resistance on both the hot and the cold surfaces. The film resistance in this connection is best expressed in dimensionless units by comparing the film resistance ($1/h$) with the thermal resistance of the wall. In the electrical analogy the resistance representing the film ($1/h$) is to be compared with the total resistance R of the circuit. The ratio of film-resistance to wall resistance is called the relative film resistance m. Symbolically, this is expressed by $m=1/hR$, where $1/h$ is converted to ohmic units.

In extreme cases the wall can be of metal with high thermal conductivity and the ambient can be still air. In such cases the value of m can rise quite high, say to more than 1,000. With so high an m the temperature difference between hot and cold surface of the piece is at any time so small that the number of sections used has no measurable influence. In fact *measurable* differences occur only at fairly low values of m. Therefore, a series of tests was run, gradually increasing the values of m, that is, increasing the hot- and cold-surface relative film resistances. It was found that beyond $m_h=6$ no appreciable voltage (temperature) differences occur within the circuit. Even for values of m in the order of magnitude of two the differences are not large. This can be seen by comparing Figures 11 and 12 with Figures 13 and 14. Figures 11 and 12 refer to $m_h=2$, $m_c=1$, while Figures 13 and 14 refer to $m_h=m_c=0$.

In Figures 11 and 13 the results for two and three sections are compared with those for 12 sections (12 sections being assumed to be identical with the theoretical curve). In Figures 12 and 14 the results for four and five sections are compared with those for 12 sections.

By comparing the group of curves for $t/RC_t=0.100$ (Figure 13) with those for $t/RC_t=0.1026$ (Figures 13, 14) it becomes evident that the difference between the curves for few sections and those for 12 sections is smaller for large values of m. A number of experiments have been carried out using different combinations for m_h and m_c. For space economy, these cannot be reproduced here. It is sufficient to say that the differences between "many"

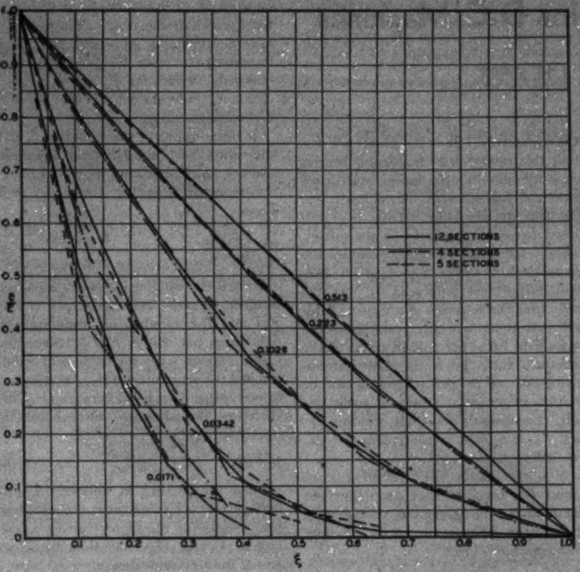


Figure 14. Voltage (temperature) rise in the circuit (wall)

Relative film resistance $m_h = m_c = 0$. The figures on the curves indicate the dimensionless time, t/RC_t

and "few" lumps were consistently smaller for large m's than for $m_c = m_h = 0$. Generally speaking, m_c is of smaller influence than m_h.

4. CONCLUSIONS

1. Two or three lumps are not sufficient to represent a problem except for very approximate solutions or for values of t/RC_t at or near the steady state. The steady-state or near-steady-state t/RC_t would be small for low m's in comparison with steady-state t/RC_t for large m's.

2. Five lumps give very good results except for relatively small values of t/RC_t.

3. In general a small number of lumps is sufficient if voltages (temperatures) at greater distance from beginning of the circuit (the hot surface) and at later times after start (that is, at higher values of t/RC_t) are necessary.

V. General Conclusions

In order to counteract the inaccuracies due to leakage, a small number of lumps is desirable. In order to counteract inaccuracies due to lumping, a large number of lumps is desirable. A practical medium is between six and nine lumps; then none of the influences become large.

The selection of nonuniform sections or lumps does not seem to influence the accuracy. Lumps can therefore be made so as to give a great number of readings in that part of the body to be investigated which is of greatest interest.

The preceding considerations hold only for the problem mentioned. Conditions may change in different types of problems, either adding resistors and capacitors to simulate two-dimensional heat flow, or in problems where parts of the circuits are kept at elevated voltages for some time.

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A Design Method for D-C Machines

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INTEREST in d-c machines in many phases of the war effort prompts the writers to present a method of design which has been found to be very useful and time saving. The method makes use of fundamental relationships in somewhat different forms and approaches the design from a different viewpoint than is ordinarily the case. The procedure has been in use for some time in the design of low-voltage aircraft motors and normal-voltage integral horsepower motors and generators. Comparative design and test results have shown good agreement.

The most common type of design problem is one which makes use of standard mechanical parts from which the required performance is realized by proper proportioning of the windings. Even though the rating required is a rather unusual one, it is rarely economically feasible to make a complete new design, mechanically and electrically Or, if a new mechanical design is made, future requirements will make it necessary to use the same frame for other ratings.

If the mechanical design is fixed, dimensional details of the magnetic circuit (with the possible exception of stacking) are known, and the magnetization curve and rotational losses become known or can be calculated by conventional methods.

While the design method is developed and illustrated herewith for a series motor with commutating poles, the principles are applicable and can be extended easily to include shunt and compound machines.

Design Considerations

In the conventional design of a d-c motor, when the magnetization curve is given, the usual procedure is to select an operating point on the curve, thereby fixing the flux. On assuming a value of generated voltage, it is then possible to calculate the number of series conductors required in the armature winding. In order to check the performance of the machine, it is necessary to make a further assumption of armature current; this in reality involves an assumption of machine efficiency. As will be demonstrated later, the selection of an operating point on the magnetization curve fixed the ratio of field ampere-turns to armature ampere-turns per pole. It is a well-known fact that this ratio has an important bearing on commutation, iron losses, and distribution of copper losses, hence, heating in the field and armature. Thus, it is proposed to select this ratio at the outset, thereby fixing the operating point on the magnetization curve for a given full-load output (see Appendix I). At the same time the selection of a ratio of field-to-armature ampere-turns establishes the series-field turns per pole in terms of the armature turns per coil. The commutating-pole turns per pole also can be written in terms of armature turns per coil. Expressions for these winding ratios F_w and F_{wc}, respectively, are developed in Appendix II.

In a d-c motor the applied voltage minus the brush-contact drop must be balanced by the generated voltage plus the voltage drop in the series windings and armature. Thus

$$V-V_{BR}=E+I_aR_s \tag{1}$$

The generated voltage is

$$E=\left(\frac{2\phi PS_c\text{ rpm}}{a\times 60\times 10^8}\right)T=BT \tag{2}$$

This is the usual form of the voltage equation, except that the total number of armature conductors has been replaced by its equivalent in terms of armature turns per coil (T) and commutator segments (S_c). As indicated in equation 2, E is dependent only on T, since the flux is already determined by selection of the operating point on the magnetization curve as discussed previously.

The series voltage drop is

$$I_aR_s=I_a(R_a+R_F+R_{cp}) \tag{3}$$

It is shown in Appendix III that the armature resistance (R_a) is a function of T^2 and that, when making use of the winding ratios, F_w and F_{wc}, the resistances of the series-field and commutating-pole windings are also functions of T^2.

Equation 3 then can be written

$$\begin{aligned}I_aR_s&=I_ar_sT^2\\&=(I_aTr_s)T\\&=\left[\frac{(NI)_F}{F_w}\right]r_sT\\&=Cr_sT\end{aligned} \tag{4}$$

C becomes known upon selection of the operating point on the magnetization curve, and r_s is essentially a constant for a given frame. An expression for r_s is developed in Appendix III. It is important to note that, having fixed reasonable values for F_w and F_{wc}, r_s is a constant, independent of the winding and machine rating.

It is now apparent that equation 1, with the values for E and I_aR_s from equations 2 and 4 substituted, can be solved for T, the required armature turns per coil. Thus

$$T=\frac{V-V_{BR}}{B+Cr_s} \tag{5}$$

Also, if the winding ratios are used, the number of series-field turns per coil is F_wT and the number of commutating-pole turns per coil is $F_{wc}T$. Since the useful copper areas for each field winding and the armature are also known, conductor sizes can be determined. Furthermore, the armature current can be calculated as

$$I_a=C/T \tag{6}$$

Since the winding as well as the full-load current are known now, the complete performance of the motor can be calculated. Details of calculation are illustrated in the sample design.

In conclusion, the foregoing information has proved useful in instances where the design problem involves determination of winding details and performance when the mechanical design is fixed. The method involves no new fundamental theory but yields a design directly wherein certain important factors are incorporated in such a way as to insure their ultimate realization in the performance of the machine. In addition, calculated performance will be in general in much better agreement with test results than is the case in conventional methods.

Appendix I. Determination of Operating Point on Magnetization Curve

It is assumed that the open-circuit magnetization curve and rotational-loss data, such as are shown in Figure 1, are known from test of previous designs in the particular frame in question or have been calculated by conventional methods.

The power developed in the armature of a

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d-c machine is EI_a and must equal the power output (W) plus the rotational loss (RL). The latter includes core losses as well as friction and windage and stray load loss. Thus

$$W' = W + RL = EI_a \qquad (7)$$

but

$$E = \frac{\phi PZ \text{ rpm}}{a \times 60 \times 10^8} \qquad (8)$$

hence

$$W' = \frac{\phi P \text{ rpm } ZI_a}{a \times 60 \times 10^8} \qquad (9)$$

The armature ampere-turns per pole, $(NI)_a$, are equal to $ZI_a/2aP$. Equation 9 then may be written

$$W' = \frac{\phi P^2 \text{ rpm}}{3 \times 10^9} \times (NI)_a \qquad (10)$$

It is desired to use the ratio of field ampere-turns to armature ampere-turns per pole, γ, as a starting point in the design. Hence, if $(NI)_a$ in equation 10 is replaced by $\gamma/(NI)_F$ and if terms are rearranged

$$\phi(NI)_F = \frac{W'}{\text{rpm}} \times \frac{\gamma}{P^2} \times 3 \times 10^9 = A \qquad (11)$$

Thus, the operating point on the magnetization curve is fixed by the output of the machine, speed, number of poles, and the ampere-turn ratio. The choice of a suitable value for γ for a given design will depend on the type of machine and the application. The range is usually from 0.6 for series machines to 1.25 for shunt machines. A good average value for shunt or compound machines with commutating poles is 1.0.

Every point on the magnetization curve corresponds to a certain value of $\phi \times (NI)_F$. A particular value is required to meet the rating requirements. One obvious method for determining the operating point would be to plot a curve of $A = \phi(NI)_F$ versus $(NI)_F$ and read from this curve the value of $(NI)_F$ for the particular value of A which is required. In obtaining such a curve, the full-load magnetization curve should be used.

The flux per pole (ϕ) in equation 11 is the full-load value and will be less than that obtained from the no-load curve for given field excitation due to the demagnetizing effects of armature reaction. If the brushes are shifted from the no-load neutral, the direct demagnetizing ampere-turns of the armature are

$$(NI)_{dm} = \frac{\theta}{90}(NI)_a = \frac{\theta}{90}\frac{(NI)_F}{F_w} \qquad (12)$$

Various methods are available for determining the demagnetizing effect of the cross-magnetizing ampere-turns, most of which are quite tedious to apply and not well adapted for use in a design method such as this. For small machines, a simple expression

$$(NI)_{dcm} = \frac{1.1}{10^4}\frac{(NI)^2_F}{F_w} \qquad (13)$$

has been found to give quite satisfactory results. If equations 12 and 13 and the no-load magnetization curve are used, it is possible to construct full-load magnetization curves for any given values of θ and γ. However, as illustrated later in the sample design, there is little need for obtaining the full-load curve, since the required point on the curve can be calculated rather easily.

Appendix II. Winding Ratios, F_w and F_{wc}

The winding ratios F_w and F_{wc} are defined as the ratios of series-field turns per pole to armature turns per coil and commutating-pole turns per pole to armature turns per coil, respectively. Thus, F_w is equal to N_f/T and F_{wc} is equal to N_{cp}/T. An expression for F_w can be found in terms of known quantities from the definition of γ as

$$\gamma = \frac{(NI)_F}{(NI)_a} = \frac{N_F I_a}{\dfrac{ZI_a}{2aP}} = \frac{aP}{S_c}\frac{N_F}{T} \qquad (14)$$

or

$$\frac{N_F}{T} = F_w = \frac{\gamma S_c}{aP} \qquad (15)$$

In a similar manner, F_{wc} is found to be

$$F_{wc} = \frac{N_{cp}}{T} = \frac{\sigma S_c}{aP} \qquad (16)$$

Where σ is the ratio of commutating-pole ampere-turns per pole to armature ampere-turns per pole. A common value for σ is about 1.25.

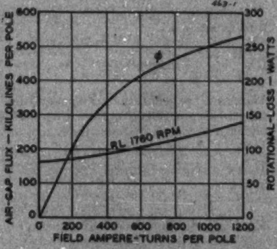

Figure 1. Magnetization and rotational-loss curves

Appendix III. Resistances in Terms of T

(*a*). Armature resistance, R_a.

The usual expression for the resistance of the armature at 25 degrees centigrade, in terms of resistivity, number of series conductors, and cross-section area of each, is

$$R_a = \frac{0.880 l_a Z}{CM_a a^2} \text{ ohms} \qquad (17)$$

If Z is written in terms of T and the number of commutator segments, and the area of each conductor is replaced by the useful slot area divided by the number of conductors per slot, equation 17 becomes

$$R_a = \frac{0.880 l_a (2S_c T)}{\left(\dfrac{S_s CM_{sa}}{2S_c T}\right) a^2} = \left[\frac{3.52 l_a S_c^2}{S_s CM_{sa} a^2}\right] T^2 \text{ ohms} \qquad (18)$$

(*b*). Series-field resistance, R_F.

The series-field resistance at 25 degrees centigrade is

$$R_F = \frac{0.880 l_F N_F P}{CM_F} \text{ ohms} \qquad (19)$$

Again the area of each conductor is replaced by an expression involving the useful copper area for the field winding, and $F_w T$ is substituted for N_F. Then

$$R_F = \frac{0.880 l_F P (F_w T)}{CM_{SF}/F_w T} = \left[\frac{0.880 l_F P F_w^2}{CM_{SF}}\right] T^2 \text{ ohms} \qquad (20)$$

(*c*). Commutating-pole-winding resistance, R_{cp}.

The resistance of the commutating-pole windings at 25 degrees centigrade is

$$R_{cp} = \frac{0.880 l_{cp} N_{cp} P_c}{CM_{cp}} \text{ ohms} \qquad (21)$$

When similar substitutions are made to those suggested in *b*, equation 19 becomes

$$R_{cp} = \left[\frac{0.880 l_{cp} P_c F_{wc}^2}{CM_{sc}}\right] T^2 \qquad (22)$$

The summation of equations 18, 20, and 22 is the total series-winding resistance of the machine at 25 degrees centigrade, R_s. Hence

$$R_s = R_a + R_F + R_{cp} = \left[\frac{3.52 l_a S_c^2}{S_s CM_{sa} a^2} + \frac{0.880 l_F P F_w^2}{CM_{FS}} + \frac{0.880 l_{cp} P_c F_{wc}^2}{CM_{sc}}\right] T^2 \qquad (23)$$

$$= r_s T^2 \qquad (24)$$

In using equation 24 a correction factor for temperature should be applied, since the expression as given is at 25 degrees centigrade.

Numerical Example

If the basic equations derived in the foregoing discussion are used, the details of a typical design will be illustrated. It is required to determine the winding and full-load performance for a 250-volt 2.0-horsepower 1,800-rpm d-c series motor in a standard National Electrical Manufacturer's Association frame 224.

The necessary mechanical details for the design, taken from previous designs in the same frame, are as follows:

Number of main poles $(P) = 2$
Number of commutating poles $(P_c) = 1$
Mean length of turn in main field $(l_F) = 18.0$ inches
Available copper area per coil in main field $(CM_{SF}) = 1{,}400{,}000$
Mean length of turn in commutating-pole winding $(l_{cp}) = 11.5$ inches

Available copper area per coil in commutating-pole winding $(CM_{sc}) = 1{,}000{,}000$ circular mils

Number of armature slots $(S_s) = 29$

Number of commutator segments $(S_c) = 58$

Mean length of armature conductor $(l_a) = 8.50$ inches

Available copper area per slot $CM_{sa} = 100{,}000$ circular mils

The open-circuit magnetization curve and rotational-loss data for this particular frame are given in Figure 1.

From the rotational-loss curve, a preliminary estimate of rotational loss for this motor is made as 130 watts. The output is 2.0 horsepower or 1,492 watts. Thus, when equation 7 is used

$$W' = W + RL = 1{,}492 + 130 = 1{,}622 \text{ watts}$$

Next, a suitable value of the ampere-turn ratio (γ) is chosen as 0.87. Then, if equation 11 is used

$$A = \frac{W'}{\text{rpm}} \times \frac{\gamma}{P^2} \times 3 \times 10^9$$
$$= \frac{1{,}622}{1{,}800} \times \frac{0.87}{4} \times 3 \times 10^9$$
$$= 0.588 \times 10^9 = \phi \times (NI)_F$$

If the material following equation 11 is kept in mind, the full-load flux per pole (ϕ) and full-load ampere-turns per pole in the field $(NI)_F$ can be determined now. The full-load magnetization curve will be somewhat below that shown in Figure 1 and approximately 1,100 ampere-turns will be required to produce a flux per pole of 520,000 lines. The product of these two figures is essentially the required value of A.

To determine demagnetizing ampere-turns it is necessary to consider only the demagnetizing effect of the cross-armature ampere-turns, since the angle of brush shift θ is zero. Thus, from equation 13 the demagnetizing ampere-turns per pole are $1.1 \times (1{,}100)^2/0.87 \times 10^{-4}$ or 153. Subtracting this from the original value of $(NI)_F$ gives 950 ampere-turns, and the corresponding value of flux from the magnetization curve is 495,000. But $1{,}100 \times 495{,}000$ does not equal 0.588×10^9, and it is apparent that $(NI)_F$ must be increased. Let the gross field ampere-turns be 1,170. Then the demagnetizing ampere-turns are 160, the net field excitation is 1,010 and the flux is 503,000. The product of 1,170 and 503,000 gives the required value for A. Thus, the full-load operating point on the magnetization curve has been fixed. Now the preliminary estimate of rotational loss can be revised on the basis of a more accurate knowledge of the field ampere-turns, and the aforementioned process can be repeated if more accuracy is desired.

The foregoing procedure for determining the full-load flux and field excitation at first may seem rather complicated and time consuming. However, it has been found that after a little practice the operation becomes very simple and fast.

The next step is to calculate the winding ratios F_w and F_{wc} from equations 15 and 16

$$F_w = \frac{\gamma S_c}{aP} = \frac{0.87 \times 58}{2 \times 2} = 12.6$$

$$F_{wc} = \frac{\sigma S_c}{aP} = \frac{1.25 \times 58}{2 \times 2} = 18.1$$

The resistance term r_o can be determined now from equation 23

$$r_o = \frac{3.52 l_a S_c^2}{S_s C M_{sa} a^2} + \frac{0.88 l_F P F_w^2}{CM_{SF}} + \frac{0.880 l_{cp} P_c F_{wc}^2}{CM_{sc}}$$
$$= \frac{3.52 \times 8.5 \times (58)^2}{29 \times 100{,}000 \times 4} +$$
$$\frac{0.88 \times 18 \times 2 \times (12.6)^2}{1{,}400{,}000} +$$
$$\frac{0.88 \times 11.5 \times 1 \times (18.1)^2}{1{,}000{,}000} = 0.01563$$

For an average operating temperature of 65 degrees centigrade r_o is 0.018.

The constants B and C are calculated from equations 2 and 4. Thus

$$B = \frac{2\phi P S_c \text{ rpm}}{a \times 60 \times 10^8}$$
$$= \frac{2 \times 503{,}000 \times 2 \times 58 \times 1{,}800}{2 \times 60 \times 10^8} = 17.5$$

and

$$C = \frac{(NI)_F}{F_w} = \frac{1{,}170}{12.6} = 93.0$$

The required number of armature turns per coil is obtained from equation 5. A brush-contact drop of two volts is assumed

$$T = \frac{V - V_{BB}}{B + Cr_o} = \frac{250 - 2}{17.5 + 93 \times 0.018} = 13$$

The armature current can be calculated now from equation 6.

$$I_a = \frac{C}{T} = \frac{93}{13} = 7.15 \text{ amperes}$$

The number of field turns and commutating-pole turns per pole is obtained by multiplying F_w and F_{wc}, respectively, by T. Thus

$$N_F = F_w T = 12.6 \times 13 = 164$$

and

$$N_{cp} = F_{wc} T = 18.1 \times 13 = 235$$

The required conductor size in the armature winding is obtained from the useful slot area and the number of conductors per slot

$$CM_a = \frac{CM_{sa} \times S_s}{2 S_c T} = \frac{100{,}000 \times 29}{2 \times 58 \times 13}$$
$$= 1{,}925 \text{ circular mils}$$

This wire size is between numbers 17 and 18 (American wire gauge), and a choice must be made between reducing the number of turns slightly and using number 17 or using number 18 and not filling the slot. Here the former choice is made. Thus, a $12^1/_2$-turn winding using number 17 wire (2,048 circular mils) is adopted. The corrected number of armature conductors is

$$Z = 2 S_c T = 2 \times 58 \times 12.5 = 1{,}450$$

Since the value of T was reduced slightly, the series-field turns must be reduced also if the assumed value of γ is to be maintained. However, this will result in somewhat higher speed than originally assumed. Thus, again there are two alternatives, of which the former will be used. Then

$$N_F = 12.6 \times 12.5 = 160$$

and

$$N_{cp} = 18.1 \times 12.5 = 225$$

The conductor size for the series field is CM_{SF} divided by N_F and is equal to 1,400,000/160 or 8,750 circular mils.

Use three number 16 wires in parallel giving an equivalent conductor of 7,750 circular mils. For the commutating pole use 220 turns of two number 16 wires equivalent to 5,166 circular mils.

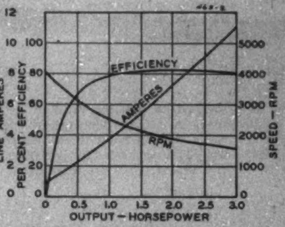

Figure 2. Performance curve for a 250-volt 2.0-horsepower 1,800-rpm d-c series motor

In order to check the motor performance using the aforementioned winding, the winding resistances are calculated in the usual manner

$$R_a = \frac{0.88\ l_a Z}{a^2 CM_a} = \frac{0.88 \times 8.5 \times 1{,}450}{4 \times 2{,}048}$$
$$= 1.325 \text{ ohms at 25 degrees centigrade}$$

$$R_a(\text{hot}) = 1.15 \times 1.325 = 1.52 \text{ ohms}$$

$$R_{fs} = \frac{0.88 l_F N_F P}{CM_F} = \frac{0.88 \times 18 \times 160 \times 2}{7{,}750}$$
$$= 0.645 \text{ ohm at 25 degrees centigrade}$$

$$R_F(\text{hot}) = 1.15 \times 0.645 = 0.75 \text{ ohm}$$

$$R_{cp} = \frac{0.88 l_{cp} N_{cp} P_c}{CM_c} = \frac{0.88 \times 11.5 \times 220 \times 1}{5{,}166}$$
$$= 0.431 \text{ ohm at 25 degrees centigrade}$$

$$R_{cp}(\text{hot}) = 1.15 \times 0.431 = 0.49 \text{ ohm}$$

and the total series resistance is

$$R_o(\text{hot}) = R_a(\text{hot}) + R_F(\text{hot}) + R_{cp}(\text{hot})$$
$$= 2.76 \text{ ohms}$$

For full-load armature current of 7.15 amperes, the $I_a R_o$ drop is 19.7 volts. Assuming a brush-contact drop of two volts, the generated voltage is 228.3 volts.

At this point the field ampere-turns, armature ampere-turns, and their ratio can be calculated and the full-load flux can be checked before determining the speed. Thus

$$(NI)_F = I_a N_F \doteq 7.15 \times 160 = 1{,}143$$

$$(NI)_a = \frac{Z I_a}{2aP} = \frac{1{,}450 \times 7.15}{2 \times 2 \times 2} = 1{,}297$$

Inverter Action on Reversing of Thyratron Motor Control

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Synopsis: Thyratron control of d-c motors offers a fast and smooth braking or reversal which is finding wider application in industry as the characteristics become better known.

With proper control the rectifier circuit, which normally supplies power to the armature of the motor, can be operated as an inverter to decelerate the motor by regenerating the rotational energy of the motor and its load into the a-c system. The circuit then shifts to rectifier operation to accelerate the motor and load to the preset speed in the reverse direction.

The motor-armature current can be held very nearly constant over the full reversal operation of deceleration and acceleration at a value which can be made less than the rated motor current. Test results and theory show that the motor's inherent reactance is sufficient for inverter operation, and, that during normal inversion, commutation of the tubes is not difficult.

In addition, inversion can be utilized to bring the motor to a stop more efficiently than by plugging and more rapidly and efficiently than by dynamic braking.

A GREAT many of the mechanized operations carried on in the industrial field call for a reversal of the mechanism by its motive power which is commonly an electric motor. In nearly all cases this reversal must not impose a severe strain on the mechanism nor demand an excessive overload from the power lines. In many of these reversing drives, particularly where they are of a cyclic nature, the reversal time is lost time and must be kept to a minimum consistent with the rating of the motor. Many of these drives call for the flexibility of the d-c motor. The d-c motors are supplied commonly from either a d-c shop bus or a motor generator set. However, an increasing number of d-c-motor applications are using the thyratron-tube rectifier to give a wide range of control, including a fast and smooth reversal.

The motor in a Ward Leonard drive is reversed by reversing the generator voltage and the current in the loop circuit. The thyratron-tube rectifier operating a motor in some respect is comparable with the Ward Leonard drive, but it has at least one important difference. The rectifier, unlike the generator, is a unidirectional conductor. It will conduct current in only one direction. Therefore, if the rectifier output voltage is reduced, the generated voltage of the motor cannot reverse the current through the circuit. Since the current through the motor-armature circuit must be reversed to reverse the direction of the motor torque, the armature connections must be reversed mechanically with respect to the tube circuit. To reverse connect the rotating motor armature suddenly into the "full-on" rectifier would amount to plugging with short-circuit current. This would be, to say the least, highly undesirable from the standpoint of the tubes, motor commutation, and the connected load. However, by proper control the tube circuit may be made to invert the d-c power generated by the motor into the a-c lines at a controlled rate.

It is not the intent of this paper to discuss the general theory nor to state a rigorous analysis of inversion, but to give a general description of the principle of inversion operating from the generated voltage of a d-c motor into the voltage of the a-c line and some means of controlling

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$$\gamma=\frac{(NI)_F}{(NI)_a}=0.881$$

Demagnetizing ampere-turns

$$(NI)_{dem}=1.1\times10^{-4}\times\frac{(1,143)^2}{0.881}=163$$

and the net field ampere-turns, $(NI)'_F$ are 980.

The full-load flux, ϕ, is 500,000 lines per pole (from curve Figure 1). When the voltage equation is used, the speed is

$$\text{rpm}=\frac{Ea\times60\times10^8}{ZP\phi}=\frac{228\times2\times60\times10^8}{1,450\times2\times500,000}$$
$$=1,885$$

Note that the speed is somewhat higher than originally assumed, since the $12^1/_3$-turn armature winding was used instead of the 13-turn winding.

The power developed in the armature is EI_a or 1,633 watts, and, from Figure 1, the rotational loss is approximately 145 watts. The motor output is then 1,488 watts, the input is 1,788 watts, and the efficiency is 83 per cent.

Test results on a motor using the aforementioned winding design are shown in Figure 2. The full-load current was 7.18 amperes, and the efficiency was 82.5 per cent. The calculated rpm was slightly high, as the test shows 1,870 rpm.

Although the winding details as shown previously are complete, it is usually desirable to check on the distribution of copper losses, current density in brushes, flux densities in various parts of the magnetic circuit, and reactance voltage of commutation.

List of Symbols

a = parallel paths in armature windings
CM_{sa} = circular mils per slot in armature
CM_{sc} = circular mils per coil in commutating winding
CM_{SF} = circular mils per coil in series-field winding
E = generated voltage in armature winding
F_w = winding ratio (for motor)
F_{wc} = winding ratio for commutating poles
I_a = armature current, amperes
I_F = field current, amperes
l_a = mean length of armature conductor
l_F = mean length of turns in series-field coil
l_{cp} = mean length of turn in commutating-field coil
N_{cp} = commutating-pole turns per pole
N_F = series-field turns per coil
P = number of main poles
P_c = number of commutating poles
R_a = armature resistance
R_{cp} = commutating-field resistance
R_F = series-field resistance
R_s = total series resistance
RL = rotational losses
rpm = speed in revolutions per minute
S_c = number of commutator segments
S_t = number of armature slots
T = armature turns per pole
V = applied volts
V_{BR} = brush-contact drop in volts
W = machine output in watts
γ = ratio of main-field ampere-turns per pole to armature ampere-turns per pole
σ = ratio of commutating-field ampere-turns per pole to armature ampere-turns per pole
ϕ = armature flux per pole

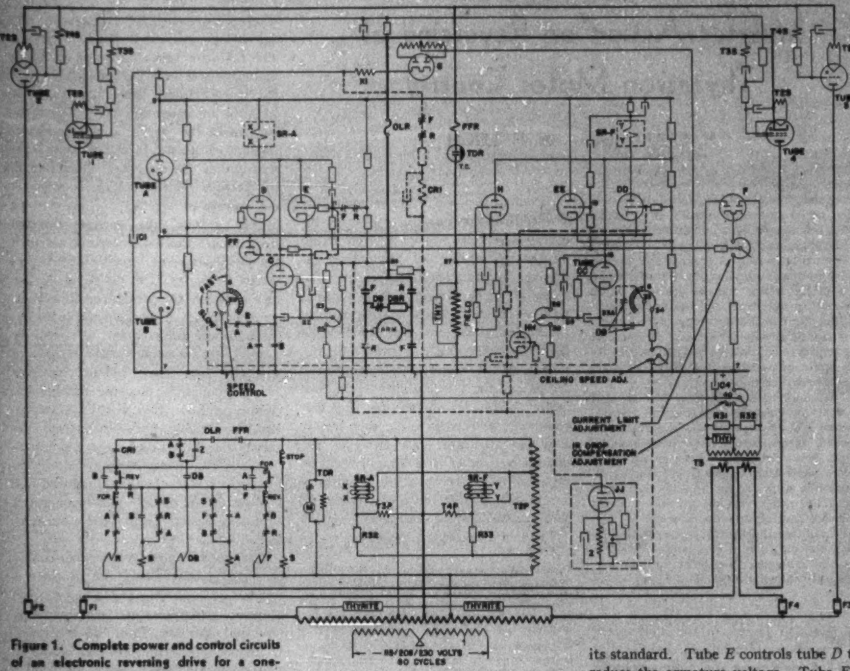


Figure 1. Complete power and control circuits of an electronic reversing drive for a one-horsepower 230-volt d-c shunt motor

this operation. Inversion is discussed from the standpoint of its usefulness in decelerating the thyratron-controlled d-c motor.

Brief Description of General Control

The method of controlling the inverter depends upon the type of rectifier circuit that is used, the method of controlling the thyratron grids, and the performance that is desired. The discussion of the circuit arrangement for the control of the inverter operation will be limited in this paper principally to one example. Since only inverter operation is to be covered here, the circuit used as an example is one the operation of which, except for inversion, was described quite thoroughly in a previous AIEE paper.[1] The complete control circuit is given in Figure 1.

Briefly, this control consists of two conventional biphase half-wave thyratron rectifiers and their associated control. Both rectifiers are operated from a single-anode transformer, one supplying the armature of the motor and the other supplying the field. The grid control of the thyratrons is by means of saturable reactor phase-shifting bridges. A small auxiliary thermionic rectifier supplies a d-c voltage, which is held constant by a pair of voltage regulator tubes, *A* and *B*, for the operation of the control vacuum tubes and for saturating the reactors.

The saturation of the armature and field saturable reactors *SR-A* and *SR-F* is controlled by two triodes, *D* and *DD*. Two voltage-amplifier tubes, *C* and *CC*, compare a portion of the armature and field voltages with an adjustable portion of a fixed voltage used as a standard and control tubes *D* and *DD* in the proper sense to regulate the armature and field voltages in proportion to the adjustments of the standards. These adjustable standards are the two speed-control potentiometers which are ganged together and so constructed and connected that they give the proper sequence of armature and field control.

Two other voltage amplifiers, tubes *E* and *EE*, compare a voltage proportional to the motor-armature current with two other fixed standards, each operating only when this current signal rises above its standard. Tube *E* controls tube *D* to reduce the armature voltage. Tube *EE* controls tube *DD* to raise the field voltage, but, since the standard of tube *EE* is slightly lower than that of tube *E*, it operates at a slightly lower current signal. The results are that, on overload of the motor, the field is strengthened before the armature voltage is reduced, and that, during acceleration of the motor, the armature voltage rises to full value before the field is permitted to weaken. The voltage proportional to the armature current is obtained by rectifying the secondary voltage of a current transformer, *T5*, the two primaries of which are connected in the anode circuits of the two armature thyratrons. It is this part of the circuit which holds the output current of the rectifier practically constant during acceleration of the motor.

General Description of Inverter Action on Reversal

As was pointed out earlier, the case of a motor driven from a rectifier is not analogous to the normal operation of a generator driving a motor, because the current cannot reverse through the rectifier. A somewhat analogous operation of

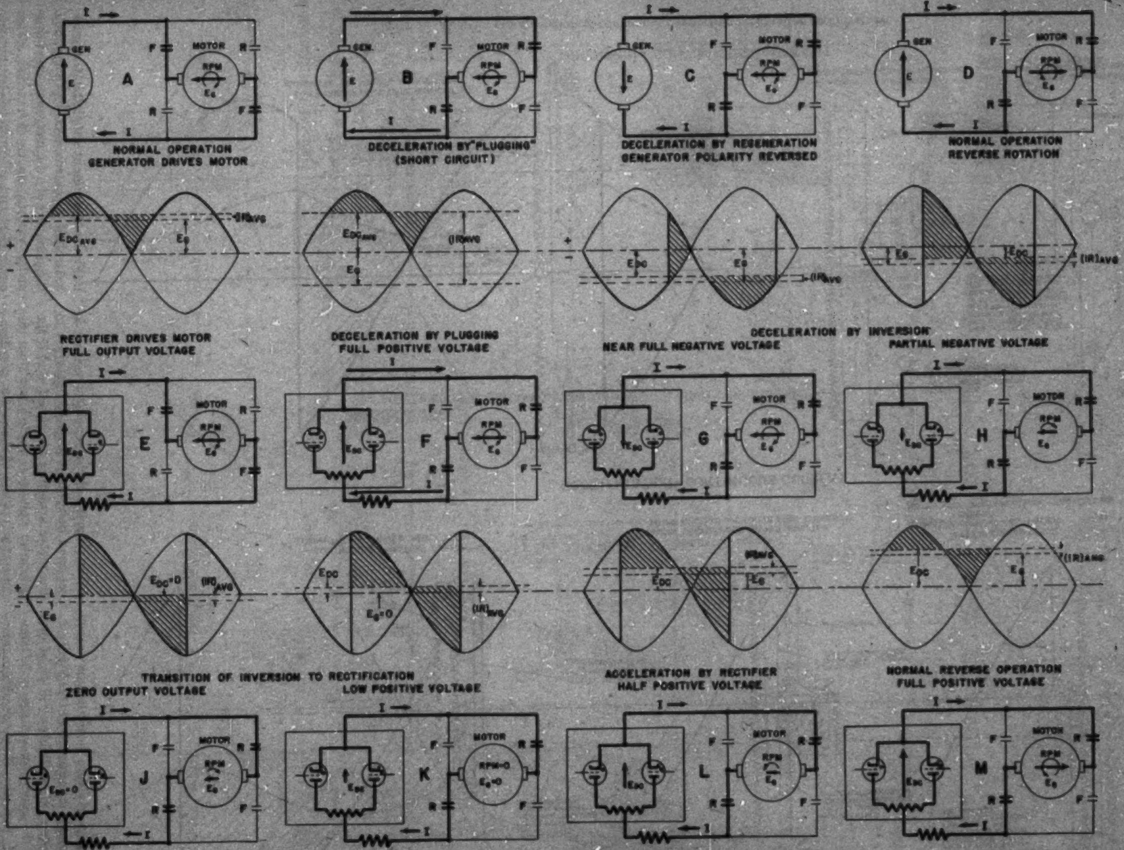

GEN.
MOTOR
RPM
E
E_g
F
R
I
A
B
C
D
NORMAL OPERATION GENERATOR DRIVES MOTOR
DECELERATION BY "PLUGGING" (SHORT CIRCUIT)
DECELERATION BY REGENERATION GENERATOR POLARITY REVERSED
NORMAL OPERATION REVERSE ROTATION
$E_{DC_{AVG}}$
$(IR)_{AVG}$
E_{DC}
+
-
RECTIFIER DRIVES MOTOR FULL OUTPUT VOLTAGE
DECELERATION BY PLUGGING FULL POSITIVE VOLTAGE
DECELERATION BY INVERSION
NEAR FULL NEGATIVE VOLTAGE
PARTIAL NEGATIVE VOLTAGE
E
F
G
H
$E_{DC}=0$
$E_g=0$
TRANSITION OF INVERSION TO RECTIFICATION
ZERO OUTPUT VOLTAGE
LOW POSITIVE VOLTAGE
ACCELERATION BY RECTIFIER HALF POSITIVE VOLTAGE
NORMAL REVERSE OPERATION FULL POSITIVE VOLTAGE
J
K
L
M
RPM=0
$E_g=0$

Figure 2. Diagrammatic representation of d-c motor reversal with constant shunt field

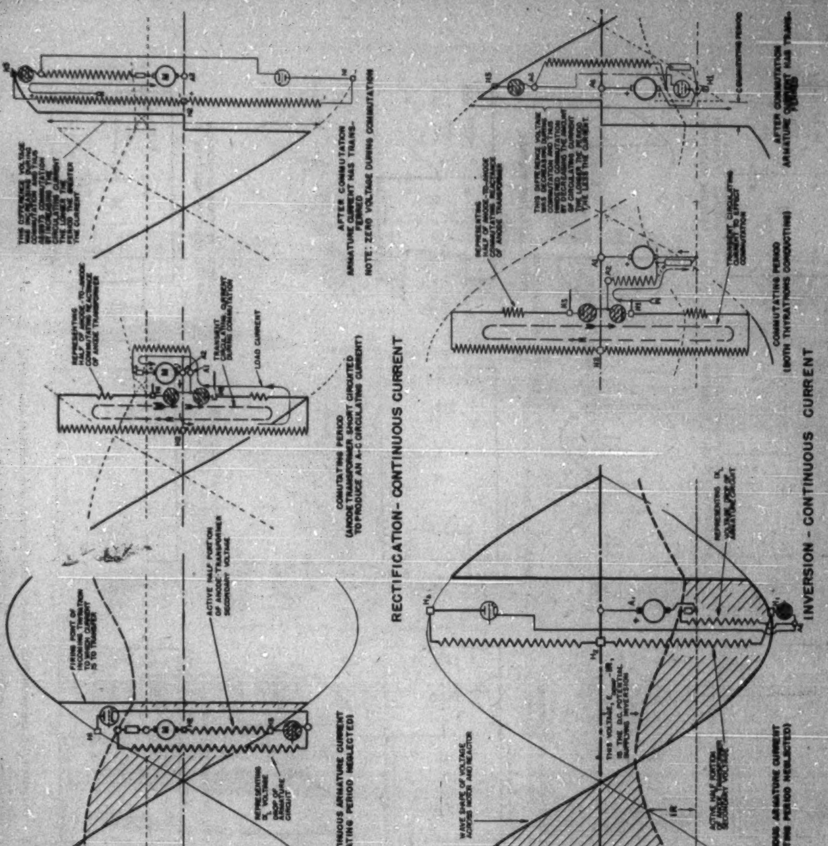

RECTIFICATION - CONTINUOUS CURRENT
INVERSION - CONTINUOUS CURRENT
CONTINUOUS ARMATURE CURRENT (COMMUTATING PERIOD NEGLECTED)
COMMUTATING PERIOD (ANODE TRANSFORMER SHORT CIRCUITED TO PRODUCE AN A-C CIRCULATING CURRENT)
AFTER COMMUTATION ARMATURE CURRENT HAS TRANSFERRED
NOTE: ZERO VOLTAGE DURING COMMUTATION
COMMUTATING PERIOD (BOTH THYRATRONS CONDUCTING)
FIRING POINT OF INCOMING THYRATRON TO WHICH CURRENT IS TO TRANSFER
ACTIVE HALF PORTION OF ANODE TRANSFORMER SECONDARY VOLTAGE
REPRESENTING IR VOLTAGE DROP OF ARMATURE CIRCUIT
REPRESENTING HALF OF ANODE-TO-ANODE COMMUTATING REACTANCE OF ANODE TRANSFORMER
TRANSIENT CIRCULATING CURRENT DURING COMMUTATION
LOAD CURRENT
WAVE SHAPE OF VOLTAGE ACROSS MOTOR AND REACTOR
THIS VOLTAGE E - IR IS THE D.C. POTENTIAL SUPPLYING INVERSION
E GENERATED
IR
M
H1
H2
A1
A2

Figure 3. Graphic representation of continuous conduction rectification and inversion

a generator driving a motor can be drawn as pictured in diagrams *A* to *D* in Figure 2.

A study of these figures will point out that, if the generator voltage is left unchanged, and the motor-armature connections are reversed, plugging and short-circuit current are the results; whereas, if the polarity of the generator voltage is reversed before the reverse contactors to the motor armature are closed, and if the generator voltage is made lower than the generated voltage of the motor, regenerative braking results.

The remainder of the diagrams, *E* to *M*, in Figure 2 represent simple biphase grid-controlled thyratron rectifiers supplying the armature of a d-c motor and operating under various stages of reversal. The motor field and all control is left out for simplicity. For the present the field excitation is assumed constant at its

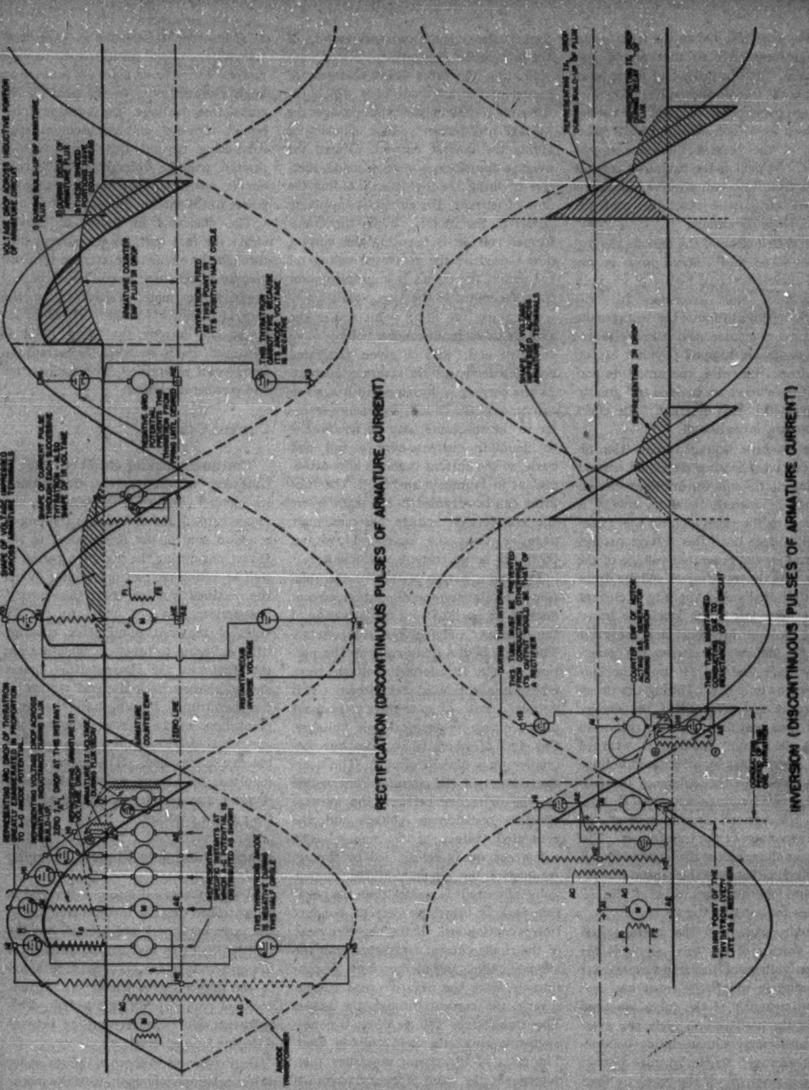

VOLTAGE DROP ACROSS INDUCTIVE PORTION OF ARMATURE CIRCUIT
① DURING BUILD-UP OF ARMATURE FLUX
② DURING DECAY OF ARMATURE FLUX
③ THESE SHADED PORTIONS HAVE EQUAL AREAS
ARMATURE COUNTER EMF PLUS IR DROP
THYRATRON FIRED AT THIS POINT IN IT'S POSITIVE HALF CYCLE
THIS THYRATRON CANNOT FIRE BECAUSE IT'S ANODE VOLTAGE IS NEGATIVE
NEGATIVE GRID POTENTIAL PREVENTS THIS THYRATRON FROM FIRING UNTIL DESIRED
SHAPE OF CURRENT PULSE THROUGH EACH SUCCESSIVE THYRATRON - ALSO SHAPE OF IR VOLTAGE
SHAPE OF VOLTAGE IMPRESSED ACROSS ARMATURE TERMINALS
REPRESENTING ARC DROP OF THYRATRON GREATLY EXAGGERATED IN PROPORTION TO A-C ANODE POTENTIAL
REPRESENTING VOLTAGE DROP ACROSS ARMATURE INDUCTANCE DURING FLUX BUILD-UP
ZERO I_AR_A DROP AT THIS INSTANT
REPRESENTING ARMATURE IR VOLTAGE DROP
ARMATURE IR VOLTAGE DURING FLUX DECAY
REPRESENTING SPECIFIC INSTANTS AT WHICH A-C POTENTIAL IS DISTRIBUTED AS SHOWN
THIS THYRATRON'S ANODE IS NEGATIVE DURING THIS HALF CYCLE
ARMATURE COUNTER EMF
ZERO LINE
INSTANTANEOUS INVERSE VOLTAGE
ANODE TRANSFORMER
AC
RECTIFICATION (DISCONTINUOUS PULSES OF ARMATURE CURRENT)
REPRESENTING I_AR_A DROP DURING BUILD-UP OF FLUX
REPRESENTING IR DROP DURING DECAY OF FLUX
SHAPE OF VOLTAGE IMPRESSED ACROSS ARMATURE TERMINALS
REPRESENTING IR DROP
DURING THIS INTERVAL THIS TUBE MUST BE PREVENTED FROM CONDUCTING; OTHERWISE ITS OUTPUT WOULD BE THAT OF A RECTIFIER
COUNTER EMF OF MOTOR ACTING AS GENERATOR DURING INVERSION
FIRING POINT OF THE THYRATRON (VERY LATE AS A RECTIFIER)
INVERSION (DISCONTINUOUS PULSES OF ARMATURE CURRENT)

Figure 4. Graphic representation of discontinuous conduction rectification and inversion

maximum value. In series with the rectifier output is shown a large reactor with sufficient inductance to give continuous conduction. Above each diagram is shown graphically the transformer voltage, the average value of the rectifier output voltage, the generated voltage of the motor, and the average value of the *IR* (current resistance) drop of the circuit.

At *F* in Figure 2 the motor-armature connections have been reversed with the rectifier turned full on, that is, with the grid voltage in phase with the anode voltage. The current-limiting circuit of Figure 1 would soon phase back the grid voltage, but before this correction could take place a cycle or two of severe peak currents would ensue. At *G* the grid voltage of the thyratron has been phased back approximately 150 degrees, so that the tube does not "fire" or become conductive until just before the transformer voltage connected to that anode goes negative. At the instant of firing, even though the transformer voltage is "nearing-zero-going negative," the net voltage on the tube is considerably positive because of the generated voltage of the motor. As soon as the thyratron is made conductive, this net positive voltage is applied across the reactance and the resistance in the circuit.

The distribution of the various electromotive forces and voltage drops is pictured more clearly in Figure 3, for both rectification and inversion. In this graphic representation of the voltages the armature reactance and the series reactor drops are lumped together as one IX_L drop. Likewise, the armature and circuit resistances are lumped and greatly exaggerated in this diagram. The effect of tube drop is neglected.

The positive voltage across the reactance and resistance starts the current building up through the armature circuit. During the current build-up, energy is supplied to the reactance by the transformer voltage until this voltage reaches zero and by the generated voltage of the motor. As the transformer voltage drops from its initial value, the rate of current rise becomes less, until, when the transformer voltage becomes more negative than the difference between the generated voltage and the IR drop, the current begins to decay. During the decay of current and flux, all of the energy which was supplied to the reactance during the current build-up is returned to the transformer by an induced voltage which, added to the difference between the generated voltage and the IR drop, keeps the current flowing against the negative voltage of the transformer. The average voltage across the inverter then is the difference between the generated voltage and the average value of the IR drop, as indicated in Figure 2.

Stated differently, the average terminal voltage of the tube circuit is the average voltage of the transformer over the period in which the tubes are conducting current. If the tubes are fired early in the positive half cycle, the average transformer voltage during conduction is positive; so the average output voltage is positive. If the tubes are fired at the end of the positive half cycle with no other electromotive force in the circuit to cause current to flow, the average voltage output is zero. However, if there is another electromotive force in the circuit of the proper polarity to cause the tubes to conduct current against the opposing negative voltage of the transformer, the average terminal voltage of the tube circuit is negative.

Shifting the firing point changes the period of the a-c cycle over which the tubes are conducting and thus changes the average transformer voltage opposing or aiding the flow of current. When the average transformer voltage made effective by firing the thyratron is aiding the flow of current, the circuit is supplying power to the motor. When the transformer voltage is opposing the current flow caused by the generated voltage of the motor, the circuit is inverting power from the motor to the a-c system.

Retarding the firing point makes the average tube-circuit counter voltage more negative and, with a given generated voltage, decreases the average current. As the current is decreased with a given reactance in the circuit, the energy transfer in the reactance may be insufficient to maintain current over a full half cycle, so the current becomes discontinuous, as in Figures 4 and 5. If the tube firing can be retarded to the point where the transformer voltage becomes more negative than the generated voltage, there will be no current conduction.

Perhaps a better picture of the relation of continuous and discontinuous conduction is obtained by a consideration of the current and voltage components. The firing of the tubes controls the portion of the transformer voltage wave which is effective in the circuit. This voltage consists of an average component plus multiple-frequency ripple voltages, the chief of which is at twice line frequency for a biphase circuit. The average current in the circuit is determined by the difference between the average effective transformer voltage and the generated voltage of the motor. The ripple current is determined by the reactance of the circuit. If the average value of current is greater than the negative peak of the ripple current, conduction is continuous. If the negative peak of the ripple current is greater than the average, the current becomes discontinuous, since the negative peak cannot reverse the current through the tubes. The transformer voltage then becomes ineffective until the next tube is fired. The value of the circuit reactance then determines the ratio of peak-to-average current and the conduction angle of the tubes.

With reference again to Figure 2, since the circuit is now taking energy from the motor instead of supplying it, the motor slows down, and the generated voltage drops. The grid voltage of the thyratrons is advanced to fire earlier in the cycle as at H in order to keep the average negative voltage of the inverter less than the motor. As the firing point advances to an angle corresponding to the peak of the transformer voltage, the average tube-circuit terminal voltage becomes zero. Advancing beyond this point gives a positive average voltage, and the tube circuit, operating as a rectifier, supplies power to the motor.

The diagrams in Figure 2 demonstrate the fact that, to get current flow through the circuit, the average terminal voltage of the tube circuit must be less negative (or more positive) than the generated motor voltage. Then, to increase the current the firing point is advanced, and to decrease the current the firing point is retarded, both for inverter and rectifier operation.

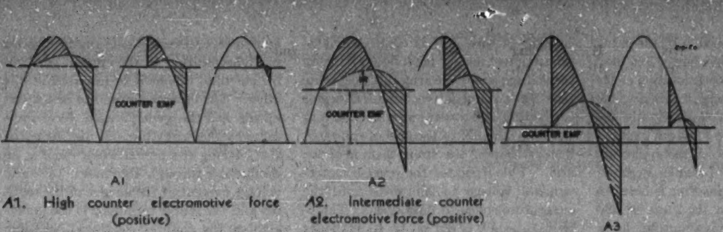
Current Control

The current-limiting circuit of Figure 1, consisting of the current transformer, its rectifier, and tube E, measures the anode current and operates in the proper sense to control the firing point of the thyratrons during the inversion period as well as during rectifier operation. When the rectified voltage from the current transformer becomes equal to the 75 volts of the voltage-regulator tube B, tube E begins to function, unsaturating the reactor in the phase-shifting circuit and so phasing back the grid voltage of the thyratrons. Phasing back the firing point increases the average negative voltage which decreases the current until a balance is reached. This circuit then regulates the firing point during both inverter and rectifier operation to hold the current constant.

Preconditioning to Prevent Plugging

If the process initiating a reversal is so controlled that the forward contacts of the reversing switch are opened, but the reverse contacts not closed until after the grid voltage has been phase-retarded to the proper angle, the severe current peaks of plugging can be avoided. This operation will be referred to as preconditioning for a reversal.

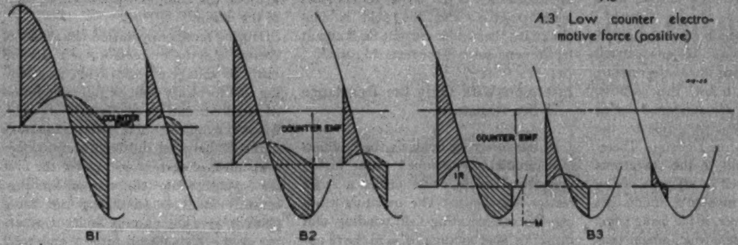
During the interval when the reversing switch is completely open, with the motor armature disconnected from the circuit, the only load across the rectifier circuit is a high-resistance circuit. Because a resistor will not keep current flowing in the circuit after the anode transformer voltage goes negative, if the thyratrons are fired anywhere in the positive half cycle, the average output voltage is



*A*1. High counter electromotive force (positive)

*A*2. Intermediate counter electromotive force (positive)

*A*3 Low counter electromotive force (positive)



*B*1. Low counter electromotive force (negative)

*B*2. Intermediate counter electromotive force (negative)

*B*3. High counter electromotive force (negative)

Figure 5. Effect of firing point and counter electromotive force on magnitude of anode current

A. Rectification—discontinuous currents

B. Inversion—discontinuous currents

M—Margin by which anode current does not become continuous and thereby tend to carry over into the region of rectification if not commutated out

positive and bears a definite relationship to the firing angle. Therefore, the positive voltage across the resistor load can be used as a measure of the firing angle. This relationship is utilized in the circuit of Figure 1 where the grid circuit of tube *E* is connected through a normally closed interlock, each on the forward and reverse contactors in series, to a tap on a resistance divider connected between a point 150 volts positive above neutral (point 5) and the positive side of the rectifier output (point 24). This serves to regulate the output voltage of the rectifier to a value which depends upon the ratio of the two sides of the resistance divider. By regulating to a low average voltage, the firing point of the thyratrons can be established at some point, such as in Figure 2G, which will give, upon closing the reverse contactor, the proper average negative voltage to limit the current to less than the current-limit value. A capacitor in the grid circuit of tube *E* "remembers" this conditioning signal momentarily, while the reverse contacts are closing, and the current is being established in the armature circuit. As this conditioning signal discharges from the grid capacitor the firing point is permitted to advance, increasing the current until the current signal takes over on tube *E*, which from there on limits the advance of the firing point to hold the current constant.

Where very fast forward and reverse contactors are used, it may be necessary to add a delay of a few cycles between the opening of the forward contacts and the closing of the reverse contacts to permit the conditioning circuit to retard the grid-voltage phase. This is accomplished by using a small d-c relay operating on the time constant of a capacitor and resistor, as *CR*1 in Figure 1.

Commutation During Inversion

In Figure 3 is indicated graphically the process of commutation in the tube circuit, for both rectification and inversion. The commutation of the current from one tube to another depends on the instantaneous voltage and the commutating reactance of the anode transformer. The greater that voltage, the faster the current can build up in the incoming tube and go out in the offgoing tube through the commutating reactance of the transformer. Commutation can take place only on a tube of more positive-anode potential.

In the case of rectification, commutation is not difficult, because it is initiated in the period when the incoming anode is going positive, or the commutating voltage is increasing. In the case of inversion, commutation of the tube currents can become troublesome, because it is initiated in the period when the commutating voltage is decreasing. If it is initiated early enough in the cycle there is sufficient voltage to commutate the current, but if the firing is retarded to very near the end of the cycle where there is little voltage the "should-be-offgoing" tube's anode may start going more positive than the "should-be-incoming" anode before commutation is complete. In that event the "should-be-offgoing" tube would take all the current again and conduct over the positive half cycle as well as the negative. This means that, while operating the biphase inverter at continuous current, the maximum retard of the firing point must be less than 180 degrees.

Maximum Voltage That Can Be Inverted

In a typical application of thyratron control to a 230-volt d-c motor, the maximum-armature speed-control setting is adjusted to give a speed corresponding to a counter electromotive force of 230 volts at full field. If then the anode transformer is designed to give an average output voltage of 280 volts, after allowing for tube drop, about 30 to 35 volts are left for *IR* compensation to hold the counter electromotive force and speed constant as the load is increased. If operated under this condition, the maximum-base-speed generated voltage that will be encountered will be 230 volts.

By comparison with rectifier operation it can be stated that, if the firing point could be retarded a full 180 degrees or to the zero voltage point and still effect commutation, with continuous conduction the negative voltage that could be developed would be equal in magnitude to the "full-on" rectifier voltage. A study of Figures 3 and 4 will show that tube drop subtracts from the rectifier voltage and adds to the negative inverter voltage which can be developed. If the firing point is to be retarded slightly less than 180 degrees to insure commutation of the current from tube to tube, it is reasonable to expect a negative voltage slightly less in magnitude than the "full-on" rectifier voltage. The results of tests indicate that the maximum possible negative voltage for a biphase inverter is between 260 and 270 volts on the basis of 280 volts maximum average output as a rectifier.

Since the maximum-negative generated voltage that will be encountered will be 230 volts, with the motor running at base speed or below, the negative motor-terminal voltage developed during inversion will be less than this by the amount of the armature *IR* drop. Therefore, the firing point of the thyratrons normally will be well in advance of the zone which would involve commutation difficulties. Moreover, if the firing point is retarded to an angle which might produce trouble with commutation, the inverter voltage with continuous conduction would be more negative than the generated voltage. In that event, the current will be discontinuous, and it will not be necessary to commutate the current from tube to tube. This holds true only if the motor-terminal voltage is limited to about 230 volts which is the case for operation up to base speed of the motor.

Reversing From Field-Weakened Speeds

When the motor is to be operated at speeds above the base speed, the field is weakened by reducing the voltage of the field rectifier. The armature voltage is held the same as at base speed, 230 volts counter electromotive force plus the *IR* compensating volts. To fulfill the condition established previously, that the generated voltage does not exceed the base-speed voltage, the build-up of the field must be controlled during inversion. To obtain the maximum decelerating torque, however, the field should be strengthened at such a rate as to hold the generated voltage constant at the base-speed value.

The normal operation of the current signal on the field control is to strengthen the field at the limiting current. On attempting to invert at the same current limit, this control would raise the generated voltage beyond all control of the inverter. In the circuit of Figure 1 this voltage increase is prevented by tube *HH* and the heavy dotted circuit connected to it. The divider to which the cathode of tube *HH* is connected is so proportioned that, if the average value of the voltage across the armature goes more than 200 volts negative, tube *HH* begins conducting and overpowers the current signal of tube *EE* to the grid of tube *DD*, thereby limiting the strengthening of the field. This regulates the reverse motor-terminal voltage to 200 volts until the motor decelerates to base speed. Thereafter, tube *HH* is cut off by its cathodes going positive, and the field is controlled by the regular current-limit circuit for the remainder of the reversal period.

Inversion With Only the Reactance of the Armature

In the foregoing analysis the rectifier-load circuit was assumed to be highly inductive, and in some places a series reactor was shown. The armature winding, the commutating field winding, and the series field winding, if used, have appreciable inductance. A typical one-horsepower 1,150-rpm 230-volt motor was found to have an inductance ranging from 0.15 henry to 0.05 henry, depending on the degree of saturation of the iron structure. This range of inductance at the 120-cycle ripple frequency of the biphase circuit is sufficient to give continuous conduction at weak field and 100 per cent current but discontinuous conduction at full field and the same current. Other motors operated from this type of thyratron rectifier operating at from 100 to 150 per cent rated current vary from near continuous to continuous conduction.

As was pointed out before, if the reactance of the circuit is fairly low, so that the peak ripple currents are high, or if the average direct current is limited to a very low value, the peak current may become greater than the average direct current and so give discontinuous conduction, as in Figure 4; that is, the current goes out in one tube before it is initiated in the other. The voltage across the motor terminals has the general wave form indicated by the heavy line. It may be noted that, during the period when there is no conduction through the tubes, the only voltage evident at the output of the rectifier and the motor terminals is the generated or the counter electromotive force of the motor.

During rectifier operation the pulses of current may be quite short over the full range of voltage at light loads, lengthening out to nearly full, or full conduction at rated current, especially at weak field, if not at full field. The inverter operates normally between full load and 150 per cent full-load current, except for the first few cycles while the current-limiting-control circuit is advancing the firing point to give the current limit. Operating at the high direct current and with the inductance ordinarily encountered in fractional and small horsepower motors, the conduction angle of the tubes in a biphase system varies from about 135 degrees to a full 180 degrees. For the larger motors with less inductance, polyphase circuits, which have lower ripple voltages, are used; hence, the conduction angle is at or near $2\pi/P$ radians.

Although ordinarily operated at high current, the circuit will invert properly with the negative generated voltage at not more than 230 volts, at an average current well below the rated motor current, from one half to one fourth, by re-

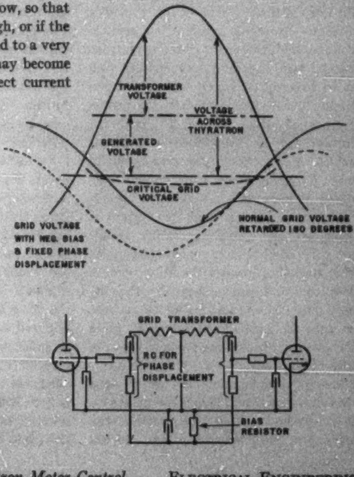

TRANSFORMER VOLTAGE
VOLTAGE ACROSS THYRATRON
GENERATED VOLTAGE
CRITICAL GRID VOLTAGE
GRID VOLTAGE WITH NEG. BIAS & FIXED PHASE DISPLACEMENT
NORMAL GRID VOLTAGE RETARDED 180 DEGREES
GRID TRANSFORMER
RC FOR PHASE DISPLACEMENT
BIAS RESISTOR

Figure 6. Alteration of grid voltage to permit firing late in the cycle

tarding the firing point to near 180 degrees. This means that on a reversal, if the pre-conditioning circuit retards the firing point to near 180 degrees while the contactors are open, the motor current upon closing the reverse contactor will be discontinuous current, as indicated in Figures 4 and 5, with an average value well below the rated value of the motor. After a cycle or two the current-limiting control will advance the firing point decreasing the opposing voltage of the inverter and increasing the average current to the limiting value. At this value it may still be slightly discontinuous or it may be continuous, as in Figure 3.

Discontinuous Operation With the Firing Retarded 180 Degrees

With discoutinuous conduction there is not the commutation problem with late firing that is present with continuous conduction, since the current in one tube goes out before the other tube is fired There is one precaution, however, that should be observed in connection with retarding the firing point a full 180 degrees. With the generated voltage of the motor reversed for inversion, the addition of the generated voltage and the transformer voltage gives a voltage across the thyratron which is positive for more than a half cycle, as shown in Figure 6. If the grid voltage is negative for only a half cycle, it cannot be retarded a full 180 degrees without causing the thyratron to fire just before or at the beginning of the positive half cycle instead of at the end of the positive half cycle as was intended. To fire at the beginning of the positive half cycle would give an extremely high peak current.

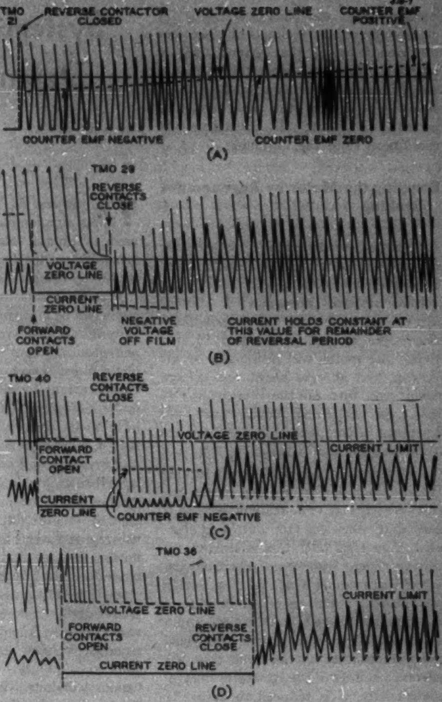


Figure 7. Oscillograms of actual reversing operation of electronic drive

This condition can be avoided by one of two means. The retard of the grid voltage during the conditioning period while the armature contactors are open can be regulated to limit the angle to a safe position a few degrees less than 180. This will still hold the current upon closing the reverse contactor to less than the limiting value, and the firing point will advance as the current-limit control takes over. Misfiring may be prevented also by altering the grid-voltage wave, so that it is negative for more than 180 degrees. In Figure 6 is shown one possible method of altering the grid voltage to permit retarding the firing a full 180 degrees. This alteration was accomplished by adding to the sine-wave voltage from the grid transformer a negative bias and displacing the phase forward to bring the firing point to the same point as with the original grid voltage. The negative bias is a self bias by grid rectification. The phase displacement is by means of a resistor and capacitor. This grid circuit is included in the circuit of Figure 1.

With a grid voltage of this characteristic the circuit which conditions the tube for a reversal can be made to shift the firing point to a maximum retard without regulating. This could be accomplished in the circuit of Figure 1 by using the *F* and *R* contacts, which now connect the grid of tube *E* to a resistor divider, to connect the same grid to the cathode of tube *E*. This would serve as a false overcurrent signal to tube *E*, possibly giving a faster conditioning.

Oscillograms of Actual Reversing Operation

In Figure 7 are shown tracings of the voltage across the armature terminals and the armature current at the beginning of a reversal. The film was hand operated, so it is slightly irregular along the time axis. Figure 7A does not include the opening of the forward contacts but does show one cycle of the preconditioned voltage. The preconditioning was adjusted to give a current at approximately the limiting value upon closing the reverse contactor. This oscillogram was taken with a one-horsepower motor with no connected load. The change of speed from fullforward to near-full reverse can be followed by the trace of the generated voltage during the nonconducting period.

Figure 7B shows a full preconditioning period and the beginning of inversion. The firing was retarded by the preconditioning circuit to give a very low armature current upon closing the reverse contactor. This oscillogram was taken with a high inertia load on the motor, so that it slowed down very slightly in the time shown.

The oscillograms in Figures 7C and D were taken during a reversal from a field-weakened speed with a high-reactance motor. In Figure 7C, the current is discontinuous at the very low value on closing the reverse contacts but becomes continuous before the current limit is reached. Note that, while the current is discontinuous, the generated voltage shows up during the nonconducting period. In Figure 7D the current is continuous from the beginning.

Stopping the Motor by Regenerative Braking

Inversion can be utilized to bring the motor to a stop by regenerative braking. If, with the motor running at any speed, a normal reversal operation is started, and at the same time the reverse-direction speed control is switched to zero speed, the motor will decelerate by inversion, but at zero speed the voltage circuit will turn off the thyratrons. If then the switching devices are deenergized, a complete stop has been accomplished.

An example of a control circuit for this type of operation is included in the circuit of Figure 1. The relaying scheme is such that, when the stop button is operated, a reversal operation is started, reversing from whichever direction the motor was running when the stop button was pushed. This reversal is exactly the same as though it were initiated from either the forward or reverse push buttons, except that at the same time the reference voltage of the armature-voltage-control tube C is reduced to zero. It may be recalled from Figure 2 that during a normal reversal the voltage output of the rectifier at the instant of zero speed is not zero but is the IR drop of the motor. If the reference voltage of the armature-voltage-control tube is at zero, and if the IR compensating signal is still in operation, the rectifier will build up a positive voltage equal to the IR drop before the voltage-control tube takes hold. Since it requires no current to hold zero speed, the control quickly retards the firing point and, as the IR signal collapses, the rectifier voltage is reduced to zero. Turning the rectifier off deenergizes the Z relay, which in turn disconnects the other switching devices.

The deceleration is at constant horsepower during the field-control range of speed and at constant torque in the armature-control range right down to the last revolution.

Polyphase Circuits

Polyphase rectifier circuits may be made to invert in the same manner as the biphase circuit and with practically the same control circuit. There is one restriction for polyphase inversion that is not present for rectification. The grid voltage of the thyratrons for inversion must remain positive for an angle of not more than $2\pi/P$ radians where P is the number of phases.

In three- and four-phase circuits the inversion current can be limited to rated motor current or less by retarding the firing point a full 180 degrees. In six-phase circuits the firing point must be retarded more than 180 degrees beyond the beginning of the positive half cycle to limit the current properly.

In polyphase circuits, commutation at 180 degrees firing is not difficult with continuous current, since the voltages of adjacent phase do not go to zero at the same time.

Reference

1. Thyratron Motor Control, E. E. Moyer, H. L. Palmer. AIEE Transactions, volume 62, 1943, November section, pages 706–12.

System Voltages and Fault Currents Determined Easily on D-C Board

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THE purpose of this paper is to outline a simple procedure that may be used to obtain short-circuit data most generally needed for relay co-ordination studies by using a d-c calculating table.

A fairly complete relay co-ordination study can be made on systems using overcurrent and directional overcurrent phase and ground relays based on the three-phase short-circuit current and the residual current for single-phase-to-ground faults. However, where ground relays have to be co-ordinated with phase relays, it is desirable to know also the value of current that flows through phase relays when ground faults occur.

Since the development of distance relays, it is also desirable to know the voltages at various locations in the transmission system during three-phase and phase-to-ground faults. Where the zero-sequence voltage is used to polarize ground relays, it would often be advantageous to know the value of zero-sequence voltage in the system at a number of points for ground faults at various locations. Where relays are applied using negative-sequence currents and voltages, it may also be desirable to know those values during ground faults. All of these currents and voltages can be calculated by the method of symmetrical components; however, the calculations are quite lengthy and require considerable time. The same information can be obtained quickly and easily by the d-c calculating board and with relatively few and simple calculations. A description of the steps required is given in the next section. This is followed by a discussion of the factors involved and an analysis of the method.

Description of Method

1. Set up the positive-sequence network on a convenient kilovolt-ampere base using reactances and impedances to give the desired accuracy.

2. Set up the zero-sequence reactances on one half the kilovolt-ampere base used for the positive-sequence network.

3. Take current readings for a three-phase fault at any point in the positive-sequence network. If possible use 100 volts on the board at all times. If it is not possible to use 100 volts, use whatever voltage that is convenient to represent 100 per cent voltage.

4. With a three-phase fault applied, read and record voltages on various busses of interest over the system by using a high-resistance voltmeter. The voltage measured from the three-phase fault to a particular bus in the positive-sequence network divided by the total board voltage and multiplied by 100 gives the per cent voltage on that bus. If the total board voltage is 100 volts, the voltmeter reading represents per cent voltage at the point being considered. Record this as V.

5. Apply a fault at a point in the zero-sequence network corresponding to that used for the three-phase fault in item 3, using the same voltage that was used for the three-phase fault. Read the total fault current and the distribution in the zero-

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sequence network on a scale having 3/2 as many divisions as the scale used for three-phase faults. These currents represent residual currents in the system and are the currents that will flow through ground relays for a phase-to-ground fault. If the 3/2 scale is not available, use the same scale that was used to read three-phase faults and multiply the results by 3/2 to give residual amperes.

6. While the phase-to-ground short circuit is still applied, use a high-resistance voltmeter and measure the voltage from the neutral bus (zero potential plane) in the zero-sequence network to the various busses of interest in the zero-sequence network. If a total board voltage of 100 volts is being used, the voltage read is the per cent zero-sequence voltage at the bus where the voltage is measured. If 100 volts is not used for the total board voltage, the reading divided by the total board voltage times 100 will give the per cent zero-sequence voltage. Record the voltage measured as V_0, a positive quantity.

7. With the phase-to-ground fault still applied, measure the voltage from the fault to the corresponding busses, in the positive-phase-sequence network, that were used to determine V_0. Record the voltage measured as V_{12}. The voltage V_{12} is the positive plus the negative-sequence voltage at the point where the voltage is measured in the positive-phase-sequence network. The reading will be in per cent if 100 volts is being used for a total board voltage. Otherwise, the reading over the total board voltage times 100 will give the per cent voltage.

8. The current which flows in the phase relays of the faulted phase during a ground fault may be calculated easily from data already taken by using the following relationship.*

$$I_{\phi 1}=\frac{I_G}{3}\left(2\frac{R_{P1}}{R_P}+\frac{R_{G1}}{R_G}\right)$$

9. The voltage at any point during a line-to-ground fault may be obtained from the following relationships. (Assume line *A* to be the faulted line.) The line-to-ground voltage expressed in per cent will be

$$E_A=V_{12}-V_0$$
$$E_B=-V_0-0.5V_{12}-J86.6$$
$$E_C=-V_0-0.5V_{12}+J86.6$$

where V_0 and V_{12} are expressed in per cent.

The line-to-line voltage expressed in per cent will be:

$$E_{AB}=E_B-E_A \qquad E_{CA}=E_A-E_C$$
$$E_{BC}=E_C-E_B$$

where all voltages are taken vectorially. Care must be taken where transformations are involved to consider the phase-angle displacement of each of the component voltages before combining them vectorially.

10. The positive- or negative-sequence component of voltage at any point during a ground fault may be obtained from the voltage readings already recorded as

$$V_2=\frac{E_G-V_{12}}{2}$$

If

$$E_G=100$$

then

$$V_2=\frac{100-V_{12}}{2}$$

$$V_1=V_{12}+V_2$$

where V_{12} and V_2 are both considered as positive quantities. The sequence components of currents in any branch of the circuit during a ground fault may be obtained from the following relationships:

$$I_1=I_2=\frac{I_G}{3}\times\frac{R_{P1}}{R_P} \qquad I_0=\frac{I_G}{3}\times\frac{R_{G1}}{R_G}$$

(I_2 and V_2 may be of interest where negative-sequence ground relays are being applied.)

Using Reactance or Impedance

When voltages are to be determined for use in determining distance relay performance, consideration should be given to whether reactance or impedance values are to be used in setting up the networks. If the ratio of reactance to resistance is fairly high, the result obtained will be sufficiently accurate if reactance is used. If the resistance is an appreciable part of the total impedance, a more accurate result may be obtained by using impedance rather than reactance. The particular system under consideration should be studied and the various constants compared to decide wisely on whether impedance or reactance should be used. It may be that in part of the network reactance values should be used and in other parts impedance values would give better results.

In the zero-sequence network it will be found usually that the ratio of X/R is high enough that reactance only may be used throughout with little error. However, for a high-resistance grounded system this, of course, would not be the case.

Choosing a Base

After the proper constants have been determined for the positive- and zero-sequence networks, a base should be chosen so as to utilize the rheostats available without having to put too many in series for a given line section nor too many in parallel for other lines, transformers, or generators. Due consideration also must be given to the range of the galvanometer readings and accuracy to which resistor settings can be made. It should be kept in mind that low-scale deflections on the galvanometer have not only a sizeable per cent error due to the observation but also an appreciable error due to the accuracy of the galvanometer, which has a guaranteed accuracy in a certain per cent of full scale.

Networks Needed

It is desirable to set up as few networks as possible and still obtain the desired information. Since the positive- and negative-sequence networks are very similar, little error results in most short-circuit studies by considering the two networks to be identical and setting them up as one network. The zero-sequence network is also necessary for ground-fault studies. Set up the positive-sequence network on the desired base and set up the zero-sequence network on a base one-half the size of that used for the positive-sequence network. These two networks are all that is required to give quite a complete study of currents and voltages for three-phase and phase-to-ground faults.

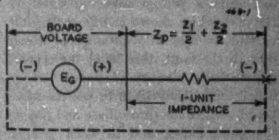

Figure 1. Three-phase fault with unit voltage and unit impedance

Unit current or base kilovolt-amperes flow to the fault

Three-Phase Faults

Consider the two networks in their most simple form as shown in Figures 1 and 2 on a per unit base. For clarification consider

$$Z_1=Z_2=Z_0=\text{unit impedance}$$

Let

$$Z_{1/2}+Z_{2/2}=Z_P$$

For a three-phase fault, if one unit of voltage is impressed on one unit of impedance, one unit of current flows, which means that base kilovolt-ampere is flowing to the fault. Therefore, the network in Figure 1 may be used to determine three-phase faults.

Phase-to-Ground Fault

Consider a simple system having unit positive-, negative-, and zero-sequence impedances. Such a system has unit three-phase fault current and unit ground-fault current. If the zero-sequence impedance is set up on one-half the base

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* See definition of symbols at the end of the paper.

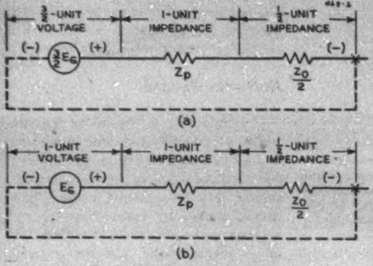

Figure 2. Phase-to-ground fault

(*a*). (3/2) unit voltage applied

$$I=\frac{(3/2)E_G}{(3/2)Z}=\frac{E_G}{Z}$$

Therefore, unit current or base kilovolt-amperes flow to the fault

(*b*). One-unit voltage applied

$$I=\frac{E_G}{(3/2)Z}=\frac{2}{3}\,\frac{E_G}{Z}$$

Therefore, two thirds of unit current or of base kilovolt-amperes flows to the fault

used for the positive-sequence network, as shown in Figure 2, then there exists 3/2 units of impedance to the fault. If 3/2 units of voltage were impressed on the network as shown in Figure 2a, unit current or base kilovolt-amperes would flow to the fault, this being the correct ground-fault current. However, it is usually more convenient to use the same board voltage for three-phase and phase-to-ground faults, and then for ground faults multiply the current readings by 3/2 or use a second scale on the galvanometer calibrated to read 50 per cent higher than the first. This second scale then reads the correct residual current, that is, the current which flows through ground relays operating on residual currents. The distribution of currents read in the zero-sequence network is the true distribution of residual current in the various branches of the network under consideration. This is apparent from the following consideration of Figures 3a and 3b.

In each figure consider $Z_1=Z_2=Z_0=Z$ for simplification.

In Figure 3a, if unit voltage is impressed on 3 units of impedance 1/3 unit current flows. This in the ordinary consideration of symmetrical components is called

$$I_1=I_2=I_0=I_G/3$$

where I_G is the residual current.

$$\frac{I_G}{3}=\frac{E_G}{Z_1+Z_2+Z_0}=\frac{E_G}{3Z}$$

or

$$I_G=\frac{3E_G}{3Z}=\frac{E_G}{Z}=\text{unit current} \tag{1}$$

Figure 3b is the same fundamentally as that shown in Figure 2b, and if unit voltage is impressed on 3/2 units of impedance, 2/3 units of current flows, that is

$$\frac{E_G}{\frac{Z_1}{2}+\frac{Z_2}{2}+\frac{Z_0}{2}}=\frac{E_G}{(3/2)Z}=(2/3)\frac{E_G}{Z} \tag{2}$$

If a multiplier of 3/2 is used or a 3/2 scale used as described previously then

$$(3/2)\times(2/3)\frac{E_G}{Z}=\frac{E_G}{Z}=I_G, \text{ the residual current}$$

Therefore, the current determined by using the 3/2 multiplier or as read on the 3/2 scale in the networks shown in Figure 2b or 3b is residual current.

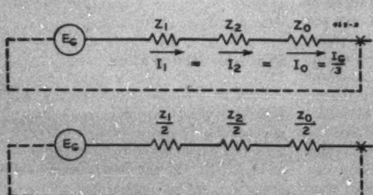

Figure 3. Residual current read directly for phase-to-ground faults

(*a*—above). Network with actual impedances

(*b*—below). Network with impedances as set on calculating table

Voltage During Three-Phase Faults

The voltage at any point in a network during a fault is the vector sum of the voltages at that point in each of the sequence networks. The voltage at any point for a three-phase fault is the generated voltage minus the *IZ* drop to the point being considered, or the *IZ* drop from the fault to the point under consideration. Since no zero- or negative-sequence currents flow during a three-phase fault, only the positive-sequence network need be considered for voltage at any point. If the voltage is measured from the fault to the point where the voltage is desired in the positive-sequence network, when a three-phase fault is applied; the value obtained is proportional to the line-to-neutral voltage at that location. The measured voltage divided by the d-c board voltage times 100 is the per cent of the system voltage that exists at the point under consideration. If the d-c board voltage is 100, the voltmeter reads per cent voltage. This is shown in Figure 4.

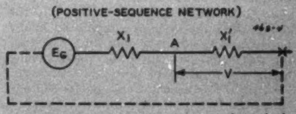

Figure 4. Voltage during a three-phase fault

The per cent voltage at point *A* is $(V/E_G)100$. If $E_G=100$ volts, then *V* is the per cent voltage at *A*

Voltages During a Phase-to-Ground Fault

When a phase-to-ground fault occurs on a grounded system, positive-, negative-, and zero-sequence currents flow, and the voltage at any point is the vector sum of the positive-, negative-, and zero-sequence voltages at that point. Each of the sequence voltages is produced by its particular sequence current flowing through that sequence impedance. The voltage in each network at a given point is the generated voltage in that network minus the *IZ* drop to the point being considered. Since the positive-sequence network is the only one having a generated voltage, the expressions for the sequence voltages at a given point, *L*, are

$${}_LE_1=E_G-I_1Z_1 \tag{3}$$

$${}_LE_2=0-I_2Z_2 \tag{4}$$

$${}_LE_0=0-I_0Z_0 \tag{5}$$

The phase-to-neutral voltage on the faulted phase, say *A*, at the point *L* is

$${}_LE_A={}_LE_0+{}_LE_1+{}_LE_2$$

From symmetrical components the voltages to neutral on the two unfaulted phases are

$${}_LE_B={}_LE\ -0.5({}_LE_1+{}_LE_2)-j0.866({}_LE_1-{}_LE_2)$$

$${}_LE_C={}_LE_0-0.5({}_LE_1+{}_LE_2)+j0.866({}_LE_1-{}_LE_2)$$

As indicated by equations 3, 4, and 5, ${}_LE_1$ is positive and ${}_LE_2$ and ${}_LE_0$ are negative for the phase-to-ground fault condition in which I_2 and I_0 are in the same direction as I_1. In the following material, therefore, the E's are used to include the significance or relative direction, whereas V's are all positive numbers and must be used with appropriate signs. Thus ${}_LE_1={}_LV_1$, and these are used interchangeably. However, ${}_LE_2=-{}_LV_2$, and ${}_LE_0=-{}_LV_0$. The E's are actually vectors, although those obtained directly from the d-c board are all in phase or 180 degrees out of phase. Thus, it is consistent later to use the symbol E in expressions for vectorial voltages of the other phases.

It is often desirable to know the voltages at several locations during a phase-to-ground fault to predict the performance of voltage relays, distance relays, undervoltage devices, or the general system disturbance. It is also often desirable to know the zero-sequence voltage at a number of locations to determine the performance of directional ground relays using zero-sequence voltage for polarization.

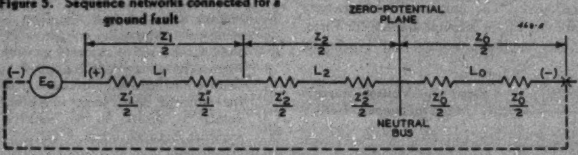

Figure 5. Sequence networks connected for a ground fault

ZERO-SEQUENCE VOLTAGE

From equation 5 above the zero-sequence voltage at the point L is the I_0Z_0 drop from the zero potential plane or neutral bus of the zero-sequence network to the point L. The current flowing in the zero-sequence network as set up in Figure 2b is $(2/3)I_G=2\,I_0$. The impedance used in this network, however, is set up on one-half the normal base used for the positive-sequence network, and the value of zero-sequence impedance in the network on a per unit base is $Z_0/2$. If two times unit current flows through one-half unit impedance, unit voltage is produced or $2I_0Z_0/2=I_0Z_0={}_LV_0$. Therefore, the voltage measured from the zero potential plane or neutral bus to point L in the zero-sequence network represents the zero-sequence voltage at the point L. If E_G is 100 volts, then $({}_LV_0/100)\,100={}_LV_0$. Therefore, if $E_G=100$, ${}_LV_0$ is the per cent zero-sequence voltage at point L. The per cent zero-sequence voltage, ${}_LV_0$, is the per cent of the maximum zero-sequence voltage which is the same as three times the normal line-to-neutral voltage.

GROUND-RELAY VOLTAGE

If the secondary or relay zero-sequence voltage corresponding to 100 per cent primary zero-sequence voltage is known, the per cent zero-sequence voltage, ${}_LV_0$ divided by 100 and multiplied by this secondary voltage will give the zero-sequence volts on the ground relay. The most common method of obtaining polarizing voltage for ground relays is to use wye-wye main potential transformers, grounded on each side, and wye-broken-delta auxiliary potential transformers with the wye grounded. With this combination the polarizing voltage on the relay can be obtained readily for the condition of 33 per cent zero-sequence voltage. This condition is simulated when the primary of one main potential transformer is disconnected from the line and short circuited with the other two transformers energized from the two remaining lines of the three-phase source.

For example, suppose the phase-to-neutral voltage is 19,000 volts, and potential is supplied to the ground relay through three 300/1 ratio wye-wye main potential transformers connected to three 1/1.73 ratio wye-broken-delta auxiliary transformers. The relay voltage for the simulated 33 per cent zero-sequence voltage condition is then

$$(19{,}000/300)1.73=110 \text{ volts}$$

or 100 per cent zero-sequence voltage corresponds to 330 volts on the relay.

It may be found helpful to record this 100 per cent secondary zero-sequence voltage on the reactance diagram at all the points where the zero-sequence voltage is of interest for future reference, thus eliminating duplicate calculations. Since various ratio auxiliary potential transformers are used often at different locations, the advantage of recording the maximum secondary zero-sequence voltage is apparent. $(V_0/100)$ 330 is the voltage on the ground relay in the aforementioned example. V_0 seldom exceeds 60 per cent for a solidly grounded system and is more often 33 per cent or less.

POSITIVE- AND NEGATIVE-SEQUENCE VOLTAGES

It will be shown first that the sum of positive- and negative-sequence voltages can be read directly from the calculating board. Figure 5 is an equivalent diagram of the network arrangement on the board with the point L identified in each network. Let E_G be unit voltage, and $Z_1=Z_2=Z_0=1$ unit of impedance.

Since unit voltage is impressed on a total of 3/2 units of impedance, 2/3 units of current flow, as was shown in Figure 2b. Since unit current is I_G, the current that flows is 2/3 $I_G=2I_0=2I_1=2I_2$.

The positive- and negative-sequence impedances being identical may be combined as shown in Figure 6, the current being the same as in Figure 5. The positive- and negative-sequence voltages during a ground fault at point L of Figure 6 are shown in Figure 7. They are derived as follows from Figures 5 and 6:

$${}_LE_1=E_G-\frac{2I_1Z_1'}{2}$$

$${}_LE_2=0-\frac{2I_1Z_1'}{2}$$

$${}_LE_1+{}_LE_2=E_G-\frac{2I_1Z_1'}{2}-\frac{2I_1Z_1'}{2}=E_G-2I_1Z_1'$$

Let

$$E_G-2I_1Z_1'={}_LV_{12}$$

$${}_LV_{12}={}_LE_1+{}_LE_2$$

Note that ${}_LV_{12}$ is the voltage that exists between the fault on the board setup and the point L in the positive-sequence network when a ground fault exists. If E_G is 100, ${}_LV_{12}$ is in per cent.

From equation 4 the negative-sequence voltage at point L is

$${}_LE_2=-I_2Z_2'$$

Since Z_2' is assumed equal to Z_1' and $I_2=I_1$, and if it is noted from Figure 7 that

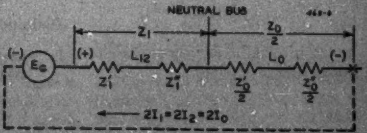

Figure 6. Sequence networks connected for a ground fault with positive- and negative-sequence networks combined

$I_1Z_1' = (E_G - {}_LV_{12})/2$, therefore

$${}_LE_2 = -I_1Z_1' = -(E_G - {}_LV_{12})/2$$

or the numerical value of negative-sequence voltage is

$${}_LV_2 = (E_G - {}_LV_{12})/2$$

It is in per cent if $E_G = 100$.

The positive-sequence voltage at point L is

$${}_LE_1 = {}_LV_{12} - {}_LE_2$$

since ${}_LV_{12}$ is equal to ${}_LE_1 + {}_LE_2$. However, E_2 is negative and has the magnitude ${}_LV_2$. Thus

$${}_LV_1 = {}_LE_1 = {}_LV_{12} + {}_LV_2$$

PHASE-TO-GROUND VOLTAGES

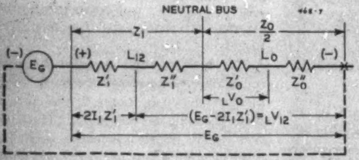

Figure 7. Voltage measurements for a ground fault

The expressions for line-to-ground voltages in terms of board measurements may be derived as follows. The per cent phase-to-ground voltage on the faulted phase, say A, at point L is

$$E_A = {}_LE_1 + {}_LE_2 + {}_LE_0 = {}_LV_{12} - {}_LV \quad \text{(since } {}_LE_0 \text{ is negative)}$$

From symmetrical components

$${}_LE_B = {}_LE_0 - 0.5({}_LE_1 + {}_LE_2) - J0.866({}_LE_1 - {}_LE_2)$$
$${}_LE_C = {}_LE_0 - 0.5({}_LE_1 + {}_LE_2) + J0.866({}_LE_1 - {}_LE_2)$$
$$({}_LE_1 + {}_LE_2) = V_{12}$$
$${}_LE_1 - {}_LE_2 = (E_G - I_1Z_1') - (0 - I_2Z_2')$$
$${}_LE_1 - {}_LE_2 = E_G$$
$${}_LE_B = -{}_LV_0 - 0.5V_{12} - J0.866E_G$$
$${}_LE_C = -{}_LV_0 - 0.5V_{12} + J0.866E_G$$

As stated previously, ${}_LV_0$ and V_{12} both are considered as positive numbers. If $E_G = 100$, the phase-to-ground voltages at point L become

$${}_LE_A = V_{12} - {}_LV_0$$
$${}_LE_B = -{}_LV_0 - 0.5V_{12} - J86.6$$
$${}_LE_C = -{}_LV_0 - 0.5V_{12} + J86.6$$

All values will be in per cent of phase-to-neutral voltage at point L.

LINE-TO-LINE VOLTAGES

The line-to-line voltages in per cent of line-to-neutral voltage will be

${}_LE_{AB} = {}_LE_B - {}_LE_A$ taken vectorially
${}_LE_{BC} = {}_LE_C - {}_LE_B$ taken vectorially
${}_LE_{CA} = {}_LE_A - {}_LE_C$ taken vectorially

Conclusion

A complete picture of currents and voltages may be determined with sufficient accuracy for most relaying problems in a system during short circuits by using a d-c calculating board with very little more time and effort than is normally used to measure currents for three-phase and phase-to-ground faults. Only one setup, using two simple networks, is required. The only readings necessary are current or kilovolt-ampere for three-phase and line-to-ground faults, including the current distribution of each desired, and V, V_{12}, V_0, for each fault condition at as many points as the voltage may be of interest.

Other values of voltages and current such as V_1, V_2, V_A, V_B, V_C, I_1, I_2, I_0, I_ϕ may be obtained by very simple calculations by using the currents and voltages that were measured. The voltage calculations are simplified by using a total board voltage of 100 volts, so that the voltmeter reads directly in per cent. The same result may be obtained where a board voltage other than 100 must be used by preparing a special scale for the high-resistance voltmeter which is marked in per cent of full board voltage, or by converting the readings taken into per cent of full board voltage.

Thus, a complete analysis of performance for modern relays may be made quickly and with sufficient accuracy for both three-phase and phase-to-ground faults by using the d-c calculating board and a high-resistance voltmeter.

Definitions of Symbols Used

I_G = total residual amperes flowing to a ground fault
$I_{\phi 1}$ = current flowing through phase relays in branch 1 for a ground fault
I_1 = positive-sequence component of current
I_2 = negative-sequence component of current
I_0 = zero-sequence component of current
Z_1 = positive-sequence impedance
Z_2 = negative-sequence impedance
Z_0 = zero-sequence impedance
R_P = reading for total three-phase fault
R_{P1} = reading for branch 1 for a three-phase fault
R_G = reading for total ground fault
R_{G_1} = reading in branch 1 for a ground fault

(Note that kilovolt-amperes or amperes may be substituted for R_P, R_{P1}, R_G, R_{G_1} if more convenient.)

E_G = unit voltage or 100 per cent voltage on the d-c board
V = voltage measured from the fault to some point where the voltage is desired in the positive-sequence network when a three-phase fault is applied
V_{12} = voltage measured on the board from the fault to some point where the voltage is desired in the positive-sequence network when a phase-to-ground fault is applied
V_0 = voltage measured on the board from the neutral bus or zero-potential plane to some point where the zero-sequence voltage is desired in the zero-sequence network when a phase-to-ground fault is applied
${}_LV_0$ = voltage that exists on the board from the neutral bus to a point of interest in the zero-sequence network designated as L

(Note that the subletter L is also used to designate various other voltages which exist at point L.)

E_1 = positive-sequence component of voltage
E_2 = negative-sequence component of voltage
E_0 = zero-sequence component of voltage
E_A = the line-to-ground voltage on phase A
E_B = the line-to-ground voltage on phase B
E_C = the line-to-ground voltage on phase C

V with subscript designates absolute magnitude of the vector E with same subscript, namely, V_2 is magnitude of E_2.

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Corona in Aircraft Electric Systems as a Function of Altitude

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Synopsis: Corona-starting voltages of the components of a 208-volt 400-cycle grounded-neutral electric system are reported for conditions to simulate altitudes between sea level and 50,000 feet. The corona characteristics are shown to be functions of air pressure, electric-field geometry, humidity, and the cleanliness of insulating surfaces. The effect of reducing pressure is shown to lower the corona-starting voltages of aircraft cables by as much as 60 per cent because the dielectric strength of large air gaps drops a comparable amount. Reducing the pressure has small effect on the corona-starting voltages of low-voltage capacitors, because the dielectric strength of small air gaps is nearly independent of pressure. Moisture and dust on insulating surfaces and moisture in aircraft cables produce corona-like disturbances below 208 volts. Data are presented on cables, capacitors, parallel studs, and needle gaps. Under dry conditions all corona-starting voltages were well above the operating voltages of the system.

CORONA in an electric system consists of minute arcs which produce ozone and nitrous acid in air; they carbonize oil, paper, and cotton; they oxidize and age rubber, and they corrode metal. Corona at operating voltages has often caused the rapid deterioration of the electric equipment containing the corona. In addition, high-frequency radiation is produced which may interfere with radio and allied instruments. For these reasons engineers working in the design and development of electric equipment should know the voltage at which corona first appears in that equipment.

Corona in insulation usually consists of the breakdown of small air pockets and gaps. It is well known that at high altitudes the density and dielectric strength of air are reduced greatly from their sea-level values. The change from sea level to 50,000 feet causes the dielectric strength of a one-centimeter parallel-plane air gap to drop by more than 75 per cent. Because of this, the question has been raised as to whether corona-starting voltages would not also drop greatly at high altitudes. This paper is an attempt to answer that question in a manner to be helpful in general design. Emphasis is placed on the specific problem of determining whether or not corona will exist in the steady-state operation of a 208-volt 400-cycle electric system.

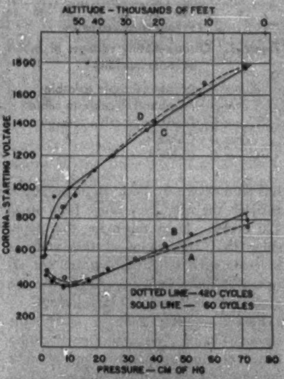

Figure 1. Effect of frequency upon the corona-starting voltage of cables

Curve *A*—Cable 1 at 420 cycles
Curve *B*—Cable 1 at 60 cycles
Curve *C*—Cable 4 at 60 cycles
Curve *D*—Cable 4 at 420 cycles

Corona measurements were made upon the following:

Aircraft cables	Figures 1, 2, 3, and 4 Table II
Capacitors	Figure 5, Table I
Meter-case back	Figures 6 and 7
Parallel studs	Figures 8 and 9
Needle gaps	Figure 10
Contaminated surfaces	Table III

Variables were pressure, humidity, voltage, and frequency.

At the close of the report, data are discussed which deal with the increase of the sparking potential of small gaps as the gap is made smaller. It is shown that this variation has great influence on the corona-starting voltage of capacitors at altitudes easily attained today.

All tests were made at room temperature and at an altitude of 1,000 feet. A method is given for correcting accurately the data on corona and breakdown in air to the low temperatures of high altitudes.

Method of Test

When corona occurs in any piece of equipment to which voltage is applied, the current to that piece of equipment includes high-frequency components. In many cases the high-frequency components can be separated from the normal charging currents, be amplified, and be viewed on the screen of a cathode-ray oscilloscope. With sufficient sensitivity at his control, an operator can thus detect the first pulses of corona and find the lowest voltage at which deterioration due to this cause can exist. A simple method for accomplishing these measurements has been described by G. E. Quinn.[1]

It is characteristic of this type of measurement that corona in a low-capacitance system, such as a needle gap, is much easier to detect than corona in a high-capacitance system. In the tests to be described in this report, the sensitivity was such that the first indication of corona was usually an off-scale deflection of the oscilloscope spot. The equipment was designed for more difficult high-capacitance work, and the results of that work check years of insulation experience. Because all of the tests in this paper were on low-capacitance samples, the writer feels that if there was any minute amount of undetected corona in the electric fields studied, it was negligible.

The tests were made with the samples in a desiccator which could be evacuated to two millimeters of mercury. Two diametrically opposite electric connections were brought into the desiccator and, when necessary, terminated in spheres. This insured that there would be no corona except in the test sample.

All tests were run at room temperature and, except when stated otherwise, with a drying agent in the desiccator. All tests were run at 60 cycles except those which were made to show that the frequency change from 400 to 60 cycles had small effect. When not stated otherwise, voltages are rms.

In this paper, corona is defined as ionization in a fluid dielectric without the breakdown of the sample in which the fluid is contained. Examples are numer-

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ous. Ionization may occur in air around metal points or in oil between transformer windings. If the test piece does not become a conductor from terminal to terminal, the ionization is defined as being corona. In air, oil, and other good dielectrics corona appears in a characteristic manner. The fluid suddenly ionizes at a certain voltage; at that voltage a large deflection of the oscilloscope spot can be seen.

In the tests involving moist samples the pulses detected were of a different type. The pulses, instead of first appearing with a large deflection, often grew from an indetectable first amplitude. This type of disturbance was also called corona because the moisture which caused the disturbance was part of the dielectric. It could not always be differentiated from ionization in air. The only tests which gave indications of ionization at voltages near 208 volts were tests with very moist samples.

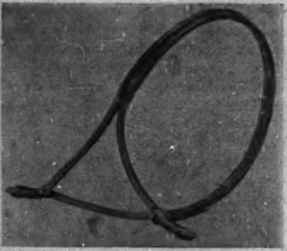

Figure 2. Form of cable-to-cable samples

The corona-starting and the breakdown curves are plotted against the pressures at which the tests were run. A second scale gives the altitudes which correspond to the test conditions insofar as corona or breakdown in *air* are concerned. As explained later in the paper the tests made at room temperature and at variable pressure have been corrected to high-altitude temperatures and pressures. The equivalent altitudes apply accurately when the ionization occurs in air, but they do not apply when the ionization occurs in a liquid such as in the moist cables. In the latter tests the equivalent conditions are doubly uncertain because the water will freeze at high altitudes unless the equipment is heated.

Frequency—Figure 1, Table I

The influence of frequency on corona in air which includes no solid barriers has been shown by Peek[3] to be negligible through the frequency range from 60 to 420 cycles. Data taken for this paper show that the corona-starting voltages of fields with solid barriers are not affected by more than ten per cent by the aforementioned change in frequency.

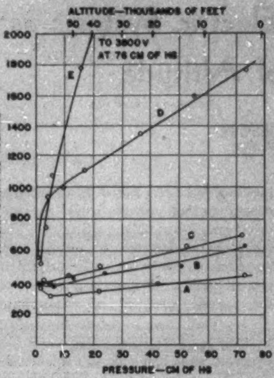

Figure 3. Corona-starting voltages of cables at room humidity

Curve *A*—Number 8 cable to cable
Curve *B*—Number 1/0 cable to cable
Curve *C*—Number 16 cable to cable
Curve *D*—Cable 4
Curve *E*—Cable 5

For example, Figure 1 shows the corona-starting voltages of two* cables tested at varying pressure and at both 60 and 420 cycles. There is no significant difference between the curves at the two frequencies.

Three types of capacitors also were checked for the present paper (Table I). The values given depend as much on the capacitor construction as on the dielectric material.

The conclusion may be drawn from Table I that the change of frequency from 60 to 420 cycles produces a small effect upon corona-starting voltages.

Cables—Figures 2, 3, 4, and Table II

Five types of cable* were tested.

1. Number 16 (19/0.0113) Deltabeston (Flamenol, asbestos, and cotton braid over tinned copper—single conductor).

2. Number 8 (7/24/0.010) Deltabeston (Flamenol and cotton braid over tinned copper—single conductor).

3. Number 1/0 (19/55/0.010) Deltabeston (Flamenol and cotton braid over tinned copper—single conductor).

4. Number 12 (19/0.018) Deltabeston (Flamenol and cotton braid over tinned copper—three conductors).

5. Number 16 (26/0.010) household appliance cord, 220-volt (rubber and cotton braid over bare copper—two conductors).

Cables 1, 2, and 3 were tested in two electric-field configurations. Cable-to-cable tests were made by binding together two equal lengths of cable (Figure 2). The two ends of each wire were connected to one of the connecting spheres. This was done to insure against corona from the free ends. Cable-to ground tests were run by laying a single large loop of the cable on a metal plane.

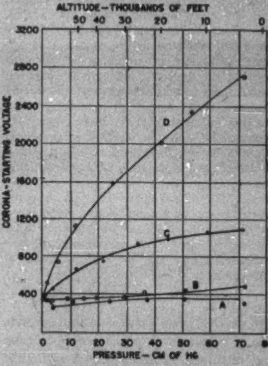

Figure 4. Effect of drying on the corona-starting voltages of number 8 cable

Curve *A*—Cable to ground, 98 per cent humidity for eight hours
Curve *B*—Cable to ground, room humidity
Curve *C*—Cable to ground, vacuum dried
Curve *D*—Cable to cable, vacuum dried

Cable 4 was a preconstructed cable with three insulated wires covered and bound together with an outer fiber wrapping. Two of the wires were connected together at one end and connected to the ground terminal of the corona detector. The third was placed at high potential. The three opposite ends were immersed in a small bottle of oil.

Cable 5 was the common 220-volt lamp and appliance cord.

Figure 3 shows cable-to-cable corona-starting voltages for cables with a moisture content as absorbed at room temperature and humidity. The term "room humidity" is not customarily used to describe the conditions of a test. However, under controlled humidities it took days for a cable to come to equilibrium. Wide variations in humidity from day to day had small effect upon the corona-starting voltage. For reference, the average humidity for the three days in which the tests of Figure 3 were run was 76 per cent. The important points to notice are that corona never appeared below 300 volts and that the 220-volt lamp cord was decidedly superior to the airplane cables insofar as corona was concerned. The thicker insulation of the appliance cord was responsible for its high corona-starting voltage.

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* Cable sizes given are American Wire Gauge numbers; the number of strands, the number of wires in each strand, and the diameter of each wire in inches are indicated in parentheses.

Figure 4 shows the effect of drying the number 8 cable by heating it under vacuum. The highest and the lowest curves on the graph were the extremes, the difference being a result of varying the moisture content from about zero to that absorbed in eight hours at 98 per cent humidity. The cables were stored and tested in desiccators, were kept dry by heat and a drying agent, or were kept at 98 per cent humidity by the presence of $CaSO_4$ and water.

The immersion in water of the number 8 cable-to-cable sample caused the corona-starting voltage to rise to 2,850 volts at 76 centimeters of mercury. This voltage was higher than the corresponding voltage on the dry-cable curve. It was seen that as the moisture content of the cables was increased the corona-starting voltages passed through a minimum. A few points on the curve, taken at room temperature and pressure, are shown in Table II.

Although it is probable that the 300-volt point is not the exact minimum, altitude and pressure have small effect. Therefore, if the cable has functioned satisfactorily at similar humidities at low altitudes, these tests indicate that it will continue to function satisfactorily at high altitudes. All cables showed similar variations with moisture content.

Figure 4 shows the number 8 cable-to-ground corona-starting voltages. It would be expected from a consideration of the electric field that the values would be half those obtained when two cables were bound together. This was roughly the case when the cables were dry (compare curves *D* and *C*, Figure 4). However, at room humidity both curves dropped to approximately the same values.

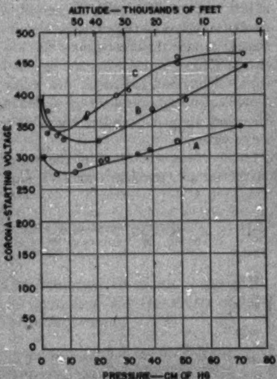

Figure 5. Corona-starting voltage of dry, nonhermetically sealed capacitors

Curve *A*—One-mil paper dielectric, not sealed or impregnated

Curve *B*—Two-mil paper dielectric, wax impregnated

Curve *C*—Two-mil paper dielectric, not sealed or impregnated

Capacitors—Figure 5 and Table I

A number of capacitors were tested at variable pressure. Typical data are shown by the three curves of Figure 5. Curves *A* and *C* were taken on capacitors which were prepared in a manner to include the most air voids possible. The paper was dry and not impregnated. The rolls were loosened to permit complete evacuation of the interior of the capacitor. Curve *B* was taken as a typical wax-impregnated sample. Upon the reduction of pressure, as the breakdown strength of large air gaps dropped by a factor of five the breakdown strength of the air voids (the corona-starting voltages of the capacitors) dropped by as little as 20 per cent. The reason for this action is given at the end of this paper in the section on the dielectric strength of air.

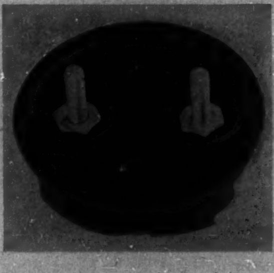

Figure 6. Meter-case back, two-inch diameter

In order that the effect of moisture could be studied, four of the unimpregnated roll capacitors were dried for 48 hours under vacuum and at 105 degrees centigrade. No change in the corona-starting voltages could be found.

It appears that the operating voltages of low-voltage capacitors which are not hermetically sealed and which are to be used at high altitude will not have to be decreased by more than 30 per cent from sea-level operating voltages even if the capacitors contain large air voids.

Meter-Case Back—Figures 6 and 7

A circular meter-case back (Figure 6) supporting two connecting bolts was also checked for corona. The bolts were mounted with an inch of separation between centers. In one test the heads and nuts were recessed; in the other they were mounted on plain surfaces. Because the curves for the two mounting methods are essentially the same, there is no advantage from the corona and the low-pressure arcover standpoint, in recessing the studs.

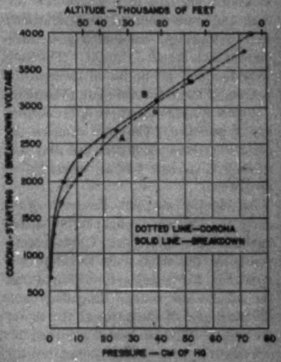

Figure 7. Corona-starting and breakdown voltage between bolts through the meter-case back of Figure 6

Curve *A*—Bolt heads not recessed

Curve *B*—Bolt heads recessed

Toward the high-pressure end of the curve, the ionization did not occur along the surface of the base or through the air between the studs. This fact was ascertained by immersing the base and studs under oil after the data for Figure 7 had been taken. Under oil the same corona-starting voltage was found at room pressure as was found in air. In fact the same type of curve of corona-starting voltage versus pressure was traced down to about 30 centimeters of mercury. The ionization actually occurred in air which was trapped in the space surrounding the studs where they passed through the insulation. At lower pressures the position of ionization shifted until breakdown appeared between the studs. Probably the point of this shift occurred at the knee of the curve.

Parallel Studs Through Plane Surfaces—Figures 8 and 9

Some additional data on studs through plane surfaces were taken. Number 4/40 studs were screwed into a one-half-inch insulating block (Figure 8). They extended one-half inch above the surface and were mounted perpendicularly in two spacings: one-fourth and three-eighths inch between threads. The leads were brought out under oil through the bottom of the insulated plate (Figure 8).

Figure 9 shows the curve of breakdown versus pressure. The only value of the corona-detection device in this test was to show that there was no corona before breakdown.

As corona was not involved, work along this line was thought to be outside of the scope of this report. Obvious weak points in the data were therefore not removed. For example, it was found that the breakdown voltage depended upon the sharpness of the edges at the top of the studs. The tip melting caused by one arcover with a 20,000-ohm resistor in series was sufficient to raise the breakdown voltage by 20 per cent. This effect probably is present in the data of Figure 9.

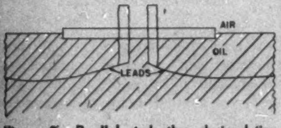

Figure 8. Parallel studs through insulating surfaces

The conclusion can be drawn that there will be no problem with *corona* on studs at practical spacings.

Needle Gaps—Figure 10

From the standpoint of corona, the worst electrode configuration that can exist is that of collinear needles facing point to point. For this reason number 8 needles at one-eighth-inch and one-fourth-inch spacing were checked for corona as a function of pressure. At high pressures corona occurred before breakdown; but, as shown on Figure 10, at low pressures breakdown occurred without first showing corona.

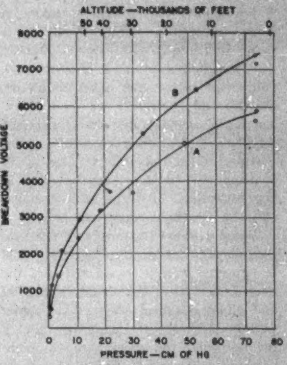

Figure 9. Breakdown voltage of the parallel studs of Figure 8

Curve *A*—One-fourth-inch spacing between threads

Curve *B*—Three-eighths-inch spacing between threads

At high altitudes the random ionization is much greater than at sea level. To increase the number of ions in the test chamber, radium (1.8 micrograms) was placed four inches away from the gap. The effects of the radium were to reduce the time between pulses of corona, to reduce the short-time breakdown voltage of the gaps, and to make the breakdown and corona-starting voltage more exactly reproducible. All data were taken in one minute step-ups, the corona-starting voltages being defined as the voltage at which the first isolated pulses of corona occurred.

It may be concluded that at spacings above one-eighth inch no corona will be encountered in any electrode configuration at or near 208 volts.

Contamination of Surface Between Studs

Data were taken on the contamination of the plastic surfaces between the studs of Figure 8. The same conditions of test were used, care being taken to keep oil from the surface by cleaning with Solvatone. The object of this section of the work was to see if corona or high-frequency discharges could possibly be produced below 208 volts.

Data were taken with $^1/_{32}$ inch of dry dust lying on the surface. As in Figure 9, no corona appeared before breakdown, and the breakdown values were not lowered by the presence of the dust.

Table I. Corona-Starting Voltage of Capacitors at 420 Cycles as Per Cent of 60-Cycle Value

	60 Cycles (Per Cent)	420 Cycles (Per Cent)
Paper-Pyranol	100	95 to 90
Mica	100	91
Unimpregnated paper	100	105

The surface was then cleaned. An open pan of warm water was placed in the desiccator. The pressure was reduced to four centimeters of mercury where the water boiled freely, depositing a thick uneven layer of moisture on everything in the desiccator. High-frequency discharges appeared at from 500 to 700 volts as the pressure was varied from 2.4 to 74 centimeters of mercury. As the water evaporated from the surface, this discharge voltage rose to 1,700 volts at 74 centimeters.

At pressures above four centimeters, the steam soon cleared up. To produce vapor at all pressures, the principle of the Wilson cloud chamber was used. A small amount of tobacco smoke was blown into the desiccator to permit a cloud to appear as the pressure was reduced. Under these conditions the discharge voltage varied from 800 to 1,700 volts, at various pressures. No curve could be plotted because the pressure had less effect than did random changes in the moisture.

Finally, dust was added to the water on the surface, forming a mass of mud

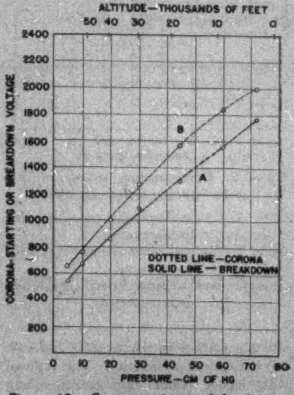

Figure 10. Corona-starting and breakdown voltage of needle gaps

Curve *A*—One-eighth-inch gap

Curve *B*—One-fourth-inch gap

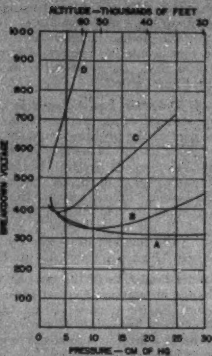

Figure 11. Breakdown strength of air at small spacings between rounded-edge, brass disks

Curve *A*—0.0004-inch gap
Curve *B*—0.002-inch gap
Curve *C*—0.008-inch gap
Curve *D*—0.040-inch gap

about $^1/_{32}$ inch thick. With this artifice, corona-like pulses appeared as low as 80 volts. A 4.5-volt Wheatstone bridge indicated a resistance of 74,000 ohms. Table III shows the type of variation found.

It is seen that these low values would probably cause trouble in the 208-volt system where they would not have affected the 28.5-volt d-c system. Because the effects of contamination were, as far as could be detected, nearly independent of pressure, it is probable that no corona difficulties will be introduced by the *change* from sea level to 50,000 feet.

The Dielectric Strength of Air as a Function of the Geometry of Electrodes

Figure 5 reveals an unexpected phenomenon. As shown by M. J. DeLerno,[1] the dielectric strength of air drops to one fourth of its sea-level value at about ten centimeters of mercury. However, the breakdown strength of the air gaps in the capacitors remained almost independent of pressure over this range. An examination of the literature of the sparking potential of air yields the following very probable reason for this apparent inconsistency.

Table II. Corona-Starting Voltage of Number 8 Cable-to-Cable Sample Versus Moisture Content

Conditions	Corona-Starting Voltage
Dried by heat and drying agent	2,500
Room humidity	450
98 per cent humidity for eight hours	300
98 per cent humidity for 76 hours	1,400
Immersed in distilled water one minute	2,800
Immersed in distilled water 60 minutes	2,850

Table III. Effect of Water on Dust-Covered Surface

Pressure—cm of Hg	73.7	1.9	16.1	30.5	41.7	73.7
Corona-starting voltage	192	130	125	137	105	80

In 1892 J. B. Peace,[6] working with J. J. Thomson, obtained data for the curves that are replotted on Figure 11. On this graph it is shown that at very small spacings, such as at 0.0004 inch, the dielectric strength of air is not affected greatly by large changes of pressure. In fact the shapes of these curves are very similar to those of the corona curves of Figure 5.

Figure 12 shows the position of the air gaps where most corona occurs in capacitors. This position is known because corona markings are left on the paper. Below the corona-starting voltage the voltage across such a gap is a definite quantity, proportional to the total applied voltage and determined by the capacitor-divider effect of the dielectrics in series. The gap is bound at each end by solid barriers and is subjected to an electric potential. The thickness of the metal foil is 0.0003 inch, and the breakdown path must be of comparable length. Therefore, the sparking potential of these gaps (and the corona-starting voltage of the capacitors) should, and do, show the same small dependence upon air pressure as did the 0.0004-inch and 0.002-inch gaps of Peace's data.

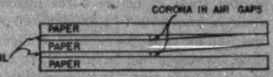

Figure 12. Section of capacitor showing position of corona at foil edge

It becomes apparent that increasing the altitude has less effect upon the corona-starting and the breakdown voltages of small air gaps than large. The maximum effect of altitude is realized for large-plane parallel gaps where the pressure and the breakdown voltages are approximately proportional.[8] The minimum effect of altitude is realized in small air voids, such as in capacitors, where the breakdown and the pressure are nearly independent. An intermediate case is shown in Figure 9 on parallel studs where the variation of breakdown is less than proportional to pressure. A second intermediate case is shown by curve *D*, Figure 4, on dry cables. The air gaps between the cable insulations are relatively large in comparison with the capacitor air voids, and the corona-starting voltage is more dependent upon pressure.

An interesting fact found by Carr[6] is that with uniform fields the sparking potential of air, regardless of its gap spacing, temperature, pressure, or random ionization, will never fall below 351 volts (the fields of Figure 11 were not quite uniform). Covering more recent work by various observers, Loeb[7] makes the following statements: "A spark will not pass below about 275 volts in air no matter what the conditions. It is most important to realize that the minimum sparking potential exists and that below it a spark cannot pass. This fact is sometimes overlooked in practical application." By the word "conditions" he refers to the pressure, the temperature, the random ionization of the air, and the spacing and shape of the electrodes. If the temperature of the electrodes rises sufficiently to cause thermionic emission, such as in vacuum tubes or in restriking a-c arcs in circuit breakers, the air will permit the incidence of an electric current far below the 275 volts mentioned.

The voltages of this discussion are peak voltages. It is seen that neither corona nor breakdown will occur in air below about 195 volts rms.

An additional interesting point arises from the foregoing data. At high altitudes corona and breakdown in a capacitor which is safely rated for sea-level operations are more likely to occur around the bushings and internal leads to the

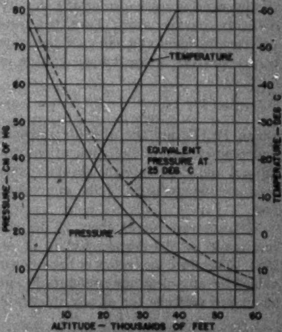

Figure 13. Change of atmospheric conditions with altitude

bushings than in the capacitor rolls. If the capacitor is completely filled with a liquid dielectric, or if it is gas filled and well sealed, the reduction of pressure with altitude will reduce the electric strength of only the external portions of the bushings. However, if the internal leads are in air and the pressure of that air drops with altitude, an unsafe condition may exist. Imagine a capacitor consisting of, for example, ten series sections of the roll of Figure 5, curve *A*. Even if the capacitor were not hermetically sealed, the corona-starting voltage of the rolls in series would remain fairly constant at about 3,000 volts, which is ten times the corona-starting voltage of the single roll. However, the breakdown voltage of the bushings and of the internal leads (if in air) would drop to below 351 volts if the pressure were reduced sufficiently.

Corrections for Low Temperatures at High Altitudes

High-altitude conditions were approximated by use of the data of Figure 13. The dotted portion of the temperature curve represents a rough average of the variable temperatures which occur in that region. No attempt was made to reproduce the low temperatures of high altitudes because Paschen's law, as stated by Cobine,[4] makes it possible to perform tests in air at room temperature which are equivalent to tests at other temperatures. Paschen's law states mathematically that for any given electrode configuration, changes in the temperature and pressure of the air will not change the sparking potential (the writer adds corona-starting voltage) unless they change the density of the air. The density of the air is proportional to the ratio of pressure to absolute temperature, and if this ratio stays constant the density will stay constant also. Thus, two tests will be equivalent and will yield the same corona-starting or breakdown voltage in air if

$$Pa/Ta = Pe/Te \qquad (1)$$

where Pa, Ta are the absolute pressure and temperature of the application conditions, and Pe, Te are the absolute pressure and temperature of the equivalent test.

All tests were run at about 25 degrees centigrade, or 298 degrees Kelvin ($Te = 298$). Therefore the corona or breakdown values for any electrode configuration in air at any altitude (or at any moderate temperature and pressure) may be found from room temperature data by substituting the pressure Pa and temperature Ta for that altitude in the following expression:

$$Pe = 298(Pa/Ta) \qquad (2)$$

The corrected voltage can be taken from the room temperature curve at pressure (Pe). In this manner the curve of equivalent pressure at 25 degrees centigrade, plotted on Figure 13, was obtained. The equivalent altitudes shown on the graphs were, in turn, taken from this curve.

For example, at 35,000 feet the temperature averages minus 54.3 degrees centigrade, or 218.7 degrees Kelvin ($Ta = 218.7$). The pressure averages 18 centimeters of mercury ($Pa = 18$). The substitution of these values in equation 2 yields an equivalent pressure of 24.6 centimeters of mercury. The effect that this low-temperature correction has on corona-starting or breakdown voltages depends upon the shape and spacing of the electrodes. For large-plane parallel gaps, the change in voltage would be proportional to the change in pressure. The breakdown voltage would be increased by the ratio of 24.6 to 18, or by 36 per cent. On the three-eighth-inch parallel studs of Figure 9 the increase would be from 3,700 to 4,400, or by 20 per cent. In the capacitor of curve *A*, Figure 5, the increase would be from 285 to 292, or only 2.4 per cent. It is interesting to note that on curve *B* of Figure 5 and on curves *A* and *B* of Figure 11 the lowering of temperature at 60,000 feet *decreases* the corona-starting and breakdown voltages. Thus, it cannot be said that data taken at high-altitude pressures and at room temperature are always conservative, though this is usually the case.

Conclusions

1. Two tests revealed corona-like disturbances at voltages low enough to approach 208 volts. The tests were on moist contaminated surfaces and on moist aircraft cables. Therefore, conditions of high moisture in the 208-volt 400-cycle electric system should be avoided or investigated by means of life tests.

2. The low corona-starting voltages were nearly independent of pressure. Therefore, the change from steady-state operation at sea level to steady-state operation at 50,000 feet will produce no difficulties with corona in the proposed system.

3. The change in frequency from 60 to 400 cycles per second will produce no appreciable change in the corona-starting voltages of cables, air gaps, and capacitors. It very probably will produce no appreciable change in the corona-starting voltage of any electrode and field configuration in industry. (Figure 1 and Table I.)

4. M. J. DeLerno[1] has shown that the dielectric strength of air drops by as much as 75 per cent as the pressure is reduced from 76 to 8.7 centimeters of mercury (0 to 50,000 feet at 25 degrees centigrade). In this paper for the same change in pressure the greatest drop in corona-starting voltage (the breakdown of small air voids and gaps) was by a factor of 71 per cent (Figure 4, curve *D*, cables). The smallest drop was by about 20 per cent (Figure 5, curve *A*, capacitors).

5. In the insulation of the aircraft cables the change in moisture content from a dry condition (Figure 4, curve *D*) to that assumed at room humidities (Figure 3, curve *A*) produced a drop in corona-starting voltage by as much as 84 per cent.

6. In low-voltage capacitors which are not hermetically sealed and contain air voids, it appears that the corona-starting voltage will not be reduced more than about 30 per cent even if the capacitors be elevated above 60,000 feet (Figure 5). No trouble should be expected from corona in the capacitor rolls at high altitudes, but the corona-starting and breakdown voltages of the bushings will be greatly decreased.

7. Tests were made with parallel studs extending perpendicularly from a clean insulating surface at spacings of one-fourth and three-eighths inch between threads. No corona existed before breakdown at pressures from 1 to 76 centimeters of mercury (Figure 9).

8. Between needle gaps the air would sustain steady-state corona without breakdown at high pressures. At low pressures, breakdown would occur without any sign of preceding corona (Figure 10).

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Polarized-Light Servo System

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Synopsis: A new servo system has been developed in which the connecting link between the primary and secondary shafts is polarized light whose plane of polarization rotates with the primary shaft. This makes possible the accurate following of the rotation of the primary shaft without any load being imposed on it. This servo system has been applied successfully in several electro-mechanical calculating machines.

SERVO mechanisms or follow-up systems are used in many recording and control devices to amplify small forces or torques so that the motion of a heavily loaded output mechanism will correspond closely to the motion of a very low torque input element.[1-3]

Polarized-Light Follow-up System

In an electromechanical calculating machine, it was desired to control the motion of an output mechanism to correspond closely to the rotation of a small integrating wheel which rolled on the surface of a revolving turntable.

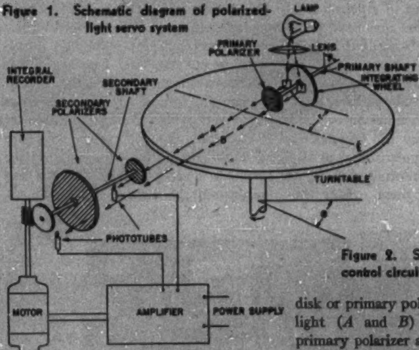

Figure 1. Schematic diagram of polarized-light servo system

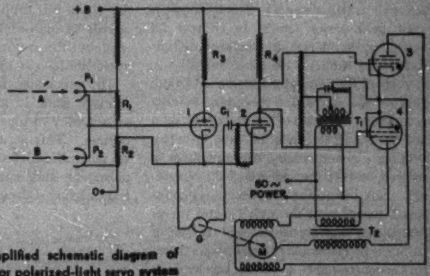

Figure 2. Simplified schematic diagram of control circuit for polarized-light servo system

The requirements, not fully met by existing follow-up systems, were:

1. The follow-up system must impose no load or restraint on either the integrating wheel or its bearings.
2. It must permit free axial movement of the wheel.
3. It must operate to follow continuously the rotation of the wheel in either direction.
4. It must be able to follow rapid accelerations and decelerations of the wheel.

To fulfill these requirements, a servo system was developed which makes use of polarized light[4] as the connecting link between the input and output mechanisms.

Principle of Operation

The principle of operation of the polarized-light servo system is shown diagrammatically in Figure 1. In this system a small integrating wheel rolls on the surface of a turntable, at an adjustable radius *r*.

Attached to the same shaft (primary shaft) and rotating with this shaft and integrating wheel is a light polarizing disk or primary polarizer. Two beams of light (*A* and *B*) passing through the primary polarizer are polarized similarly, and the plane of polarization rotates as the primary shaft rotates.

A secondary shaft is, in effect, coupled to the primary shaft by means of these two rotating beams of polarized light in the following manner. The two beams of light fall on two phototubes after each has passed through a separate secondary or control polarizer. These two secondary polarizers are attached to the secondary shaft in such a manner that their planes of polarization are at right angles to each other.

When the primary polarizer is oriented so that its plane of polarization is half way between, or at an angle of 45 degrees with both of the secondary polarizers, equal amounts of light will strike both phototubes. If the plane of polarization is not at 45 degrees to both secondary polarizers, unequal amounts of light will strike the phototubes. Thus, if the primary polarizer is rotated from balance a small amount in one direction, the light on one phototube is reduced because the angle between the two polarizers in that beam becomes more nearly 90 degrees; at the same time, the angle between the planes of polarization in the second beam becomes more nearly zero, with a corresponding increase in light on the second phototube. If the primary polarizer is rotated in the other direction, the light beams are unbalanced in the other direction.

The unbalanced output of the phototubes is amplified and controls the speed and direction of rotation of the servomotor, and thereby the secondary shaft and polarizers, to keep the light on the phototubes always at or near balance.

There are four positions of the primary shaft with respect to the secondary shaft where the light beams may be balanced. Only two of these positions, however will give a condition of stable balance. Once the condition of stable balance has been established it is maintained, and the secondary shaft will rotate with the primary shaft in either direction within the allowable limits of speed, acceleration, and load as determined by the motor and amplifier characteristics.

The axis of the primary shaft is held by bearings parallel to the plane of the turntable surface. For changes in radius of contact of the integrating wheel, the primary shaft and polarizer can be moved axially along the two light beams without materially affecting their balance.

Electronic Circuit

Figure 2 is a simplified schematic diagram of the control circuit. The two light beams fall on phototubes P_1 and

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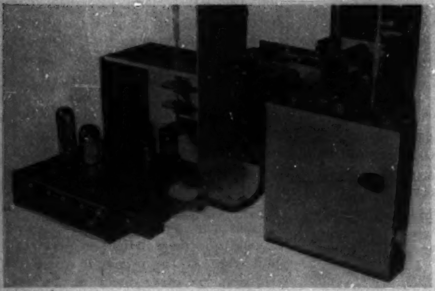

Figure 3. Polarized-light servo system for integrating pressure traverse recorder

P_2 which form two legs of a bridge circuit comprised of R_1, R_4, and the resistances of the two phototubes. The voltage unbalance of this bridge is applied between grid and cathode of vacuum tube 1. This tube together with tube 2 and resistances R_3 and R_4 form a second bridge circuit whose output is applied to the grids of two thyratron tubes 3 and 4.

The anode currents of the thyratrons, supplied from the 60-cycle power source through transformer T_2, flow through a split field series motor to control its speed and direction in response to unbalance of the light beams A and B.

A small d-c generator is attached to the shaft of the motor, and its output voltage is applied through a capacitor C_1 between the grid and cathode of tube 2. The voltage e generated by this generator is proportional to the speed ω of the secondary shaft. For the frequencies encountered, the current through C_1, and therefore the voltage across the grid resistor of tube 2, is proportional to de/dt and is therefore proportional to $d\omega/dt$ the acceleration of the output shaft. Thus, the condition of balance of the two light beams is anticipated, and the system is stabilized and remarkably free from the hunting or oscillation commonly experienced in servo systems.

It will be noted that a single lamp is used as a light source for both beams of light so that any changes in the light output due to aging, darkening of the bulb, and input voltage affect equally the light falling on both phototubes, causing no unbalance of the system. The bridge circuits employed also make the electronic circuits relatively free of unbalance because of supply voltage fluctuations.

The torque-angle characteristics of the polarized-light servo system are dependent on the characteristics of the amplifier and motor. Within operating limits the torque is approximately proportional to the relative angular displacement of the light polarizers at any given speed. Series-motor characteristics make possible very high torques for rapid acceleration and deceleration. No accurate measurements of maximum accelerations and decelerations have been made, but it is estimated that the primary shaft can accelerate or decelerate at 2,000 rpm per second or more before the secondary shaft will drop out of synchronism. Speeds as high as 1,500 rpm have been used, but this is not necessarily the upper speed limit.

The secondary shaft will follow the primary shaft in angular position with an accuracy of plus or minus one or two degrees. This accuracy will be affected by nonuniformity of polarization in the polarizers and nonuniformity of light transmission through them. Since the primary and secondary polarizers normally operate at 45 degrees to each other, the rate of change of light transmission with respect to angular displacement is a maximum. Therefore, the angular errors due to variations in the polarizers are at a minimum.

Applications

INTEGRATION

The polarized-light servo system is being used successfully in new electromechanical instruments for solving accurately problems involving integration. Refer to Figure 1; the radius r at which the integrating wheel rolls on the turntable is variable by means of a cam or screw and represents the integrand.

If θ is the angular displacement of the turntable, the angular displacement of the integrating wheel and primary shaft ρ is proportional to $\int r d\theta$.

$$\rho = k\int r d\theta$$

The angular displacement of the secondary shaft is the same as that of the primary shaft and integrating wheel, and since considerable power is available to drive this shaft, it can be loaded to drive other calculating and recording mechanisms. The primary shaft is held in alignment by jewel bearings and is balanced so that radial and thrust loads are substantially zero. Since there can be no restraining torque on the primary shaft because of loading of the secondary shaft, the integrating wheel rolls on the turntable with negligible slippage and correspondingly high accuracy.

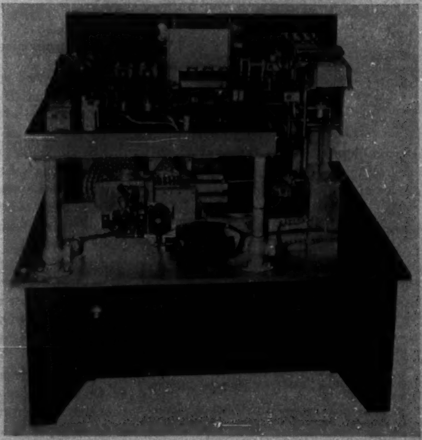

Figure 4. Integrator unit (without cover) for automatic integrating pressure traverse recorder. Rear view

Figure 3 shows a polarized-light servo system as used in the new General Electric integrating pressure traverse recorder, a specialized calculating machine which employs two follow-up systems as integrators. The phototube amplifier is tipped down to show its construction and the secondary polarizers.

The secondary shaft is at right angles to the primary shaft, and the light beams are changed in direction by means of mirrors. The phototube amplifier for the second integrator is shown at the lower right in operating position.

Figure 4 displays the same machine which shows the cams for moving the integrating wheels on the turntables. The driving motors and thyratron controls are on the shelf above the turntables and cams.

Figure 5 is a front view of the calculating and recording unit of the integrating pressure traverse recorder which is used by the research section of the General Electric turbine engineering division in its air test laboratory for studying flow phenomena through turbine passages. In this machine the integrated values will check each other within 0.01 per cent on repeated integration of the same function. These errors can be due largely to mechanical tolerances. This indicates that the slippage of the integrating wheel on the turntable is extremely small.

Figure 5. Integrator unit for automatic integrating pressure traverse recorder

A special integral recorder on this device makes possible the reading of integrated values to the precision required for such measurements.

Figure 6 shows the General Electric differential analyzer[8] which is used in solving problems involving the use of differential equations. This machine has fourteen integrating units, each with its polarized-light servo system.

SPEED MEASUREMENT

In another application a variation of this servo system has been used to measure accurately over a wide range the speed of a large rotating machine. The turntable is driven at a constant and accurately known speed by a synchronous motor from a regulated frequency source. The secondary polarizers are driven by the machinery whose speed is to be measured and the wheel is positioned on the turntable so that it follows the secondary polarizers; thus the radius of the wheel on the turntable is an accurate measure of the unknown speed. Speed measurements have been made to 0.025 per cent accuracy for primary shaft speeds up to 1,300 rpm.

DIFFERENTIATION

In the measurement of speed as already described, the servo system is actually used as a differentiator in which the angular displacement of the secondary polarizers ρ' may be expressed by the equation:

$$\rho' = \int \omega_1 dt$$

where ω_1 is the angular velocity of the control polarizers.

The angular displacement of the primary polarizer ρ is expressed by

$$\rho = k\int r\,d\theta$$

$$d\theta = \omega_2 t$$

where ω_2 is the angular velocity of the turntable.

$$d\theta = \omega_2 dt$$

If $\rho = \rho'$, then

$$d\rho = d\rho' = \omega_1 dt = k\omega_2 r\,dt$$

and

$$\omega_1 = \frac{d\rho'}{dt} = \frac{d\rho}{dt} = k\omega_2 r$$

Since ω_2 is constant,

$$\frac{d\rho'}{dt} = Kr$$

When used as a differentiator, the servo system is inherently oscillatory, but by taking the proper steps the oscillations can be highly damped.

Conclusions

The new servo system using plane polarized light as the interconnecting link between the primary and secondary mechanisms makes possible the driving of heavy loads with accurate following of primary shaft without imposing any restraint or reaction upon its motion, either in the direction of rotation or axially.

Figure 6. Differential analyzer showing 14 integrator-unit assemblies

Electric-Circuit Burning-Clear and Damage Phenomena on Aircraft Structures

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Synopsis: Normal and continuous performance of electric circuits is a prerequisite to the effective use of airplanes. Electric short circuits which result in the loss of power for vital control operations or which cause fires are definite hazards. The extensive use of the 28.5-volt d-c aircraft systems, the proposed 120-volt d-c system, and the consideration being given a three-phase 120–208-volt a-c grounded neutral system for high-altitude planes necessitated an investigation into the burning-clear and damage phenomena on all circuits. Short circuits were applied on an aluminum-alloy section of an airplane, and oscillograms of short-circuit current and voltage taken. In both the a-c and d-c systems it was found that the fault-damage and burning-clear phenomena depended upon the energy level in the short-circuit area. When the contact area between the conductor and aircraft structure was small, the energy level was high, and the fault burned itself clear. In such cases the aircraft structure was perforated. Increase in contact area and a consequent reduction in energy concentration resulted in a sustained fault current. In some of these cases a definite welding of the conductor to the aircraft structure occurred, while in others no burning took place.

The General Problem

IT is generally accepted that the correct performance of the electric system is vital to the effective use of an airplane. Electric-wiring faults, such as short circuits resulting from gunfire or other causes, may result in faulty control or fire hazard. Experience has shown that on the commonly used 28.5-volt d-c systems such short circuits are often self-clearing, and serious consequences do not result. However, this is not always the case, and it is possible that a significant number of airplanes have not returned from the combat area because of damage to the electrical system. Accordingly, it was felt advisable to investigate the conditions under which faults may be expected to clear and to determine the magnitude of current and length of time required to cause serious damage to the aircraft structure when a short circuit occurs. It should be recognized that, in practice, where circuit protection is used the fault duration and damage may be limited by this protective equipment which would remove the faulty circuit from the system. Such protection was not used in the tests as they were intended to show the effects of short circuit where system protection was not involved. Circuit protection is discussed in a companion paper.[1]

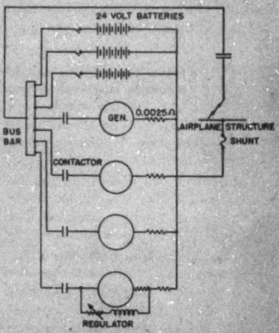

Figure 1. Aircraft electrical system as adapted for burning-clear tests

Since consideration is also being given to a three-phase 120–208-volt 400-cycle a-c grounded neutral system (120 volts to ground), and to a 120-volt d-c system, similar investigations using such power systems were carried out.

The Investigational Method

The basis of the investigation was a series of direct full power short-circuit tests made in the laboratory with a portion of aircraft structure as test piece. A diagram of connections of the circuit for the 28.5-volt d-c system is given in Figure 1. This circuit is an adaptation of a typical electric circuit for a four-engined airplane such as the "Flying Fortress" and is, as nearly as is possible, a duplicate of the actual system. Each of the four *P1* aircraft generators (200 amperes 28.5 volts) was driven by a ten-horsepower 3,600-rpm induction motor supplied from a variable frequency source. The negative terminal of each generator was connected through a resistance of 0.0025 ohm to a common point, while the positive terminals were tied to a single bus. Three batteries could be paralleled with the generators by means of single-pole switches. Time, current, and voltage values for each short circuit were determined by means of an automatic magnetic oscillograph.[2]

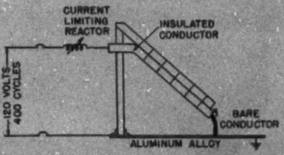

Figure 2. Arrangement of test sample for single-phase-to-ground 400-cycle burning-clear tests

As a test specimen representative of an aircraft structure, part of a wing section and nacelle of a "Flying Fortress" was used. To this was attached the negative terminal of the supply system. The positive terminal of the system was connected through a contactor to a length of 1/0 aircraft cable, one end of which rested perpendicularly on the aircraft structure; short circuits were applied by closing the contactor. The position of the conductor

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Several applications have been made in integrating and differentiating unit of special electromechanical calculating and measuring devices for solving complicated problems accurately and quickly.

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was chosen because cables running parallel to the aircraft fuselage and subsequently severed by gunfire can be bent so as to contact the structure perpendicularly. The effects of a short circuit brought about in this way are the most severe.

With the generators operating at 3,000 rpm and an open-circuit voltage of 28.5 volts, and at first with no batteries in the circuit, short circuits as previously mentioned were applied to various sections of the aircraft structure by closing in the line contactor. The supply system was then augmented by one battery, then two, and finally three batteries in parallel with the generators and the tests repeated.

A considerable number of tests was carried out with three batteries in the system, and the effects on the short circuit of load on the system, of increased short-circuit resistance, and of simulated high altitude were investigated. For the altitude tests a small section of 0.020-inch aluminum alloy and a piece of 1/0 cable touching the alloy perpendicularly were set up on a platform to which supply terminals had been connected. The structure and cable were covered with a bell jar which was evacuated to a pressure corresponding to the required altitude.

Data on the 120–208-volt a-c system were obtained in the course of separate tests in which power was obtained from three single-phase transformers supplied by means of a motor generator set. A diagram of connections for single-phase-to-ground faults is given in Figure 2. Variations in current level were obtained by means of taps on air-cored reactors connected in series with the transformer and by connecting two or three transformers in parallel. The current range investigated was selected to cover the possible number and sizes of generators operating in parallel on certain aircraft systems.

Using 0.020-inch and 0.25-inch aluminum alloy, short circuits as in the foregoing were applied at different current levels and with various sizes of conductor. Similar tests were carried out in a steel chamber evacuated to a pressure corresponding to an altitude of 40,000 feet. Further short circuits were applied to 0.10-inch magnesium alloy, at sea level and at a pressure corresponding to an altitude of 40,000 feet, and to generator end shields of magnesium alloy.

Analysis of Results

Both the d-c and a-c tests brought out very decisively that there are two widely different results of short circuit; these are referred to in the following as "self-clearing" and "weld." By the former is meant melting and vaporizing of the metal in the vicinity of the short-circuit area and its explosive expulsion from the arc, resulting in a complete clearance of the short circuit. A weld takes place when only sufficient energy is involved in the short circuit to result in a permanent connection between the conductor and airplane structure.

Oscillograms representative of self-clearing and weld faults are given in Figures 3 and 4, respectively, for the d-c system, and in Figures 5 and 6, respectively, for the a-c system. Figure 7 shows the progress of a typical short circuit while Figure 8 is illustrative of the damage to the aircraft structure as a re-

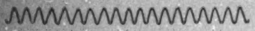
Table I

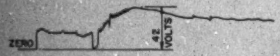
Test	System	Number of Generators	Number of Batteries	Current (Amperes)	System Condition Prior to Short Circuit	Type of Material	Cable Size (Number)	Insulation	Altitude	Results
Short circuit	28.5-volt direct current	Four	None	Up to 4,500	No load	0.020-inch to 1/8-inch Al (alloy)	1/0	Deltabeston	Sea level	Self-clearing and sustained
Short circuit	28.5-volt direct current	Four	One	Up to 4,500	No load	0.020-inch to 1/8-Al (alloy)	1/0	Deltabeston	Sea level	Self-clearing and sustained
Short circuit	28.5-volt direct current	Four	Two	Up to 4,500	No load	0.020-inch to 1/8-inch Al (alloy)	1/0	Deltabeston	Sea level	Self-clearing and sustained
Short circuit	28.5-volt direct current	Four	Three	Up to 4,500	No load	0.020-inch to 1/8-inch Al (alloy)	1/0	Deltabeston	Sea level and 40,000 feet	Self-clearing and sustained
Short circuit	28.5-volt direct current	Four	Three	Up to 4,500	Loaded	0.020-inch to 1/8-inch Al (alloy)	1/0	Deltabeston	Sea level and 40,000 feet	Self-clearing and sustained
Short circuit	28.5-volt direct current	Four	Three	Up to 4,500	No load. Resistance added to external circuit	0.020-inch to 1/4-inch Al (alloy)	1/0	Deltabeston	Sea level and 40,000 feet	Self-clearing and sustained
Short circuit	28.5-volt direct current	Four	Three	Up to 4,500	No load	0.020-inch to 1/8-inch Al (alloy)	1/0	Deltabeston, glass-cotton, 100 per cent glass	Sea level and 40,000 feet	Self-clearing and sustained
Short circuit	120-volt direct current	—	—	Up to 2,000	No load	0.020-inch to 1/8-inch Al (alloy)	1/0	Deltabeston	Sea level and 40,000 feet	Self-clearing and sustained
Short circuit	120-volt 400 cycles alternating current	—	—	Up to 2,500	No load	0.016-inch to 1/4-inch Al (alloy)	16 to 1/0	Deltabeston	Sea level and 40,000 feet	Self-clearing and sustained
Short circuit	120-volt 400 cycles alternating current	—	—	Up to 2,500	No load	0.010-inch Mg	1/0	Deltabeston	Sea level and 40,000 feet	Self-clearing and sustained
Short circuit	120-volt 400 cycles alternating current	—	—	Up to 2,500	No load	Mg end shields	1/0	Deltabeston	Sea level and 40,000 feet	Self-clearing and sustained
Short circuit	208-volt 400 cycles alternating current	—	—	3,700	No load	Copper to copper	16 to 1/0	Deltabeston	Sea level and 40,000 feet	Self-clearing and sustained
Gunfire	24-volt direct current	—	1,500 amperes capacity	Up to 1,500	No load	0.016-inch to 1/8-inch Al (alloy)	16 to 1/0	Deltabeston, glass-cotton, 100 per cent glass	Sea level	Self-clearing and sustained

sult of short circuit. Table I summarizes the test program and test results.

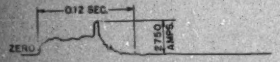
28.5-VOLT D-C SYSTEM

(a) *Effect of Batteries.* Although the electric supply system of the "Flying Fortress" airplane consists of four generators and three batteries in parallel, it was felt advisable to study the effect of varying the number of batteries. The results here compared with the general trend of results throughout all the tests. Whenever the contact pressure between the cable and aircraft structure was such that only a few strands of the cable touched the aluminum alloy, whether the latter were skin, bulb angle, or rib, the fault was self-clearing and often accompanied by heavy arcing and explosive scattering of molten aluminum-alloy particles. In most cases the fault did not burn clear until the structure was perforated. However, there were instances when the cable melted at a sufficiently high rate to open the circuit before the perforation was complete.


A. 60-cycle timing wave



B. Voltage wave



C Current wave

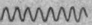
Figure 3. Oscillogram showing self-clearing d-c fault

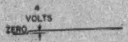
Time of short circuit 0.12 second. Maximum current 2,750 amperes. Maximum voltage 42 volts

With the pressure between the cable and airplane structure such that the contact arc was large, the short circuit was continuous and did not burn clear. In some cases a weld was produced while in others there was no visual damage either to cable or aircraft structure. As the number of batteries increased, a greater pressure was required to produce a short circuit which would not burn clear.

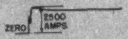
When the contact is light, only a few strands of cable form the short circuit with the airplane structure. Thus at the instant of establishment of the short circuit, the concentration of energy is extremely high, causing the copper and aluminum alloy to melt superficially and an arc to strike. This arc continues until the fault clears either because of a structural perforation, or because of rapid melting of the cable.

If the contact pressure between cable and structure is high, the contact area is much larger and, therefore, the energy concentration greatly reduced. In general this energy concentration is insufficient to cause initial melting of the cable or aluminum alloy, and no arc results; the fault is continuous and sustained. The ampere capacity of the system increases with an increase in the number of batteries and, therefore, for the short-circuit energy concentration to be maintained at a level low enough to cause continuation of the short circuit, a greater pressure is required for each battery addition. Thus a system having a small ampere capacity will probably produce more welds due to short circuit than one with a large ampere capacity, since less pressure is required in the former case.


A. 60-cycle timing wave



B. Voltage wave



C. Current wave

Figure 4. Oscillogram showing d-c short circuit resulting in weld

Maximum current 2,500 amperes

The oscillogram of Figure 3 is illustrative of a self-clearing fault. The effect of the field regulator can be seen during arcing periods, the generator voltage rising with current flow. Rapid and irregular variations in the voltage wave, caused by abrupt changes in resistivity within the arc and movement of the cathode spot, are plainly visible. When the current reaches its final zero and the fault clears, the rise of recovery voltage and its slow decay to normal open-circuit value are clearly recorded.

An oscillogram characteristic of a short circuit resulting in a weld is given in Figure 4. After the initial peak the current assumes its steady-state value, and no arcing takes place.

(b). *Effect of Load on System (Four Generators, Three Batteries).* When the system is loaded prior to the application of the short circuit, the values of short-circuit current, the resulting damage both to cable and structure and the time of short circuit are in general the same as for four generators and three batteries in parallel without load. The peak value of the short-circuit current is reached more quickly after short circuit, however, by virtue of the fact that with load on the system the main field strength is high and the armature current is already about one fifth of the value of short-circuit current.

(c). *Effect of Increased Short-Circuit Resistance.* The effect of increasing the short-circuit resistance was similar to lowering the ampere capacity of the electric system. Thus with higher values of resistance the possibility of a weld is increased. Faults remote from the airplane bus are therefore more likely to result in a weld because of current limitations due to increased circuit resistance.

(d). *Effect of Altitude.* At pressures corresponding to higher altitudes, arcing persisted somewhat longer, although current and voltage values were about the same as those obtained at sea level. Increasing contact pressure between the cable and airplane structure resulted again finally in a weld. When the short circuit resulted in perforation of the aluminum alloy the hole was more clear-cut than at sea level due to decreased oxidation. The molten alloy was scattered with explosive violence in a direction perpendicular to the structure and away from the conductor, but there was little evidence of molten copper. No appreciable change in pressure inside the bell jar was noticed during each short circuit.

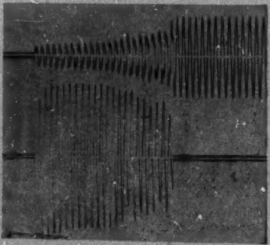


Figure 5. Oscillogram showing self-clearing a-c fault

Time of short circuit 0.052 second. Maximum current 2,000 amperes. Maximum voltage 120 volts

Upper curve—Voltage
Lower curve—Current

120-Volt D-C System

Exploratory tests at sea level and a pressure corresponding to an altitude of 40,000 feet with a 120-volt d-c system were carried out. The results followed the same trend as those obtained with the 28.5-volt d-c system and the 120–208-volt a-c system, faults either burning clear or resulting in a weld, the governing factors being as stated previously. In some cases arcs approximately half an inch long were obtained.

120–208-Volt A-C System

(*a*). *Phase-to-Ground Fault on Aluminum Alloy, at Sea Level.* With a supply system of 120–208 volts alternating current, the effects of short circuit again fell into the two categories—self-clearing and weld. When the contact between the conductor and airplane structure was light, and therefore the contact area small, whatever the size of conductor, type of structure, or current level involved, the fault was self-clearing, and an arc formed. In all cases the arcing was severe, though rarely prolonged, and was accompanied by explosive scattering of molten metal. Small fires were started within a radius of ten feet from the short-circuit area, the test floor being covered with a thin layer of sawdust and some oil.

Faults which were not self-clearing fell into the second category of a "weld or sustained short circuit." In these instances the material, or the shape of the part of the aircraft structure concerned, was not important, the principal factors being the current level and cable size.

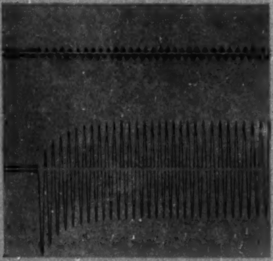


Figure 6. Oscillogram showing a-c short circuit resulting in weld

Maximum current 670 amperes

Upper curve—Voltage
Lower curve—Current

Thus, for a given current level, the smaller the conductor the greater is the contact pressure required to produce a weld. For a given conductor size, in order to produce a weld, the contact pressure required is much less the lower the current level. Short circuits occurring in remote sections of an aircraft electric circuit may readily result in welds or be sustained, unless the fault was otherwise interrupted. Figure 8 illustrates damage from short circuit to a section of an airplane. The damaged area identified by the number 8 is representative of a short circuit resulting in a weld while the remaining areas are perforations from self-clearing faults.

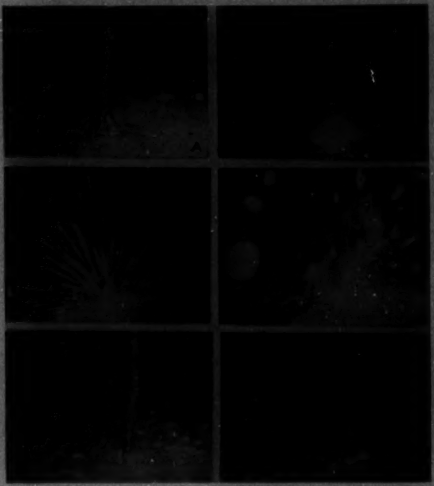


Figure 7. Progress of short circuit resulting in perforation of aircraft structure

Gunfire Tests

Parallel conductors were mounted inside and one inch from an aircraft section and connected at one end to a bus fed from a 24-volt battery. The other ends of the conductors were insulated and the aircraft section connected to the negative terminal of the battery. With a range of approximately 100 yards, 50-caliber-machine-gun bullets were fired at the test piece so that they would strike the conductor on the leaving side. In almost every case faults were produced and in one instance, as shown at *A* in Figure 9, the conductor welded to the structure. Damage both to conductor and structure, due to short circuit, was insignificant in comparison with the damage caused by gunfire.

Further bullets were fired so that they would strike a conductor on the entering side. In such cases a self-clearing short circuit occurred when the conductor contacted the structure as the former swung back into position after being severed by the bullet impact.

Whenever a conductor was severed by gunfire, about one inch of insulation was torn off on both sides of the break, and the conductor strands were spread brush fashion. Although in general only a few strands subsequently formed the short circuit, in one instance most of the cable cross section contacted the aircraft structure. In this case a weld was produced, as stated previously.

Three small areas of the aircraft section were covered with a thin coat of insulating varnish. In these areas faults occurred only when a severed conductor contacted the edge of a perforation produced by gunfire.

General Interpretation

The physical aspects of the d-c and a-c short circuits were similar, and pictures shown in Figure 7 serve to illustrate the progress of a self-clearing short circuit on either system. Evidence of explosive scattering of molten metal in Figures 7e and 7f, just before the fault clears, indicates that the formation of the perforation required a time about equal to the duration of the short-circuit current. As the short circuit progresses, the conductor melts, and a pool of molten aluminum alloy is formed; meanwhile gases in the arc throw out particles of alloy explosively. The oscillogram presented in Figure 5 indicates intermittent arcing. Examination of the conductor after a fault cleared usually showed that during the short-circuit period drops of molten copper had formed. Apparently these droplets under arc forces are elongated toward the airplane structure and finally break free from the conductor. The formation and breaking off of the droplets correspond to the striking and extinguishing of the arc. The process is repetitive until the aluminum alloy is very thin and can no longer withstand the gas pressure in the neighborhood of the arc; the perforation is then formed explosively, and the fault clears permanently.

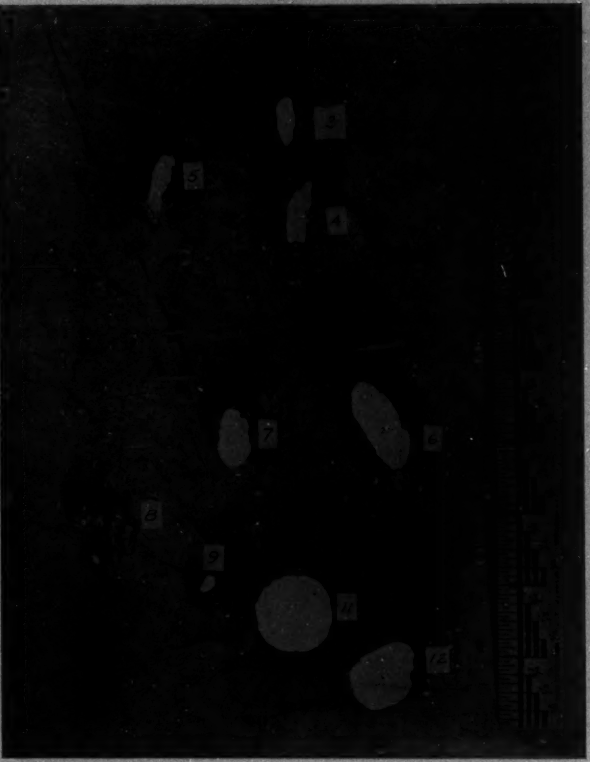


Figure 8. Electric-fault damage to aircraft structure

3,4, and 5—Self-clearing fault. 1,120 amperes, number 16 cable

6—Self-clearing fault. 2,000 amperes, number 8 cable

7 and 9—Self-clearing fault. 665 amperes, number 8 cable

8—Welded fault. 665 amperes, number 8 cable

11 and 12—Self-clearing fault. 1,970 amperes, number 8 cable

The increase in measured voltage indicated in the oscillogram of Figure 5 is due to a slight increase in arc length.

(*a*) *Phase-to-Ground Fault on Aluminum Alloy, at High Altitude.* Both the self-clearing and weld type of short circuit were substantially independent of barometric pressure corresponding to high altitude. The duration of the former type was generally the same as at sea-level pressure, and the extent of damage was unchanged except for decreased oxidation at high-altitude pressures. Because of this the perforations were more clean-cut than at sea-level pressure.

(*b*) *208-Volt Phase-to-Phase and Three-Phase Faults.* Phase-to-phase faults on various sizes of wires and three-phase faults fell into categories of self-clearing and weld, the current level and contact pressure affecting both as before. In those instances when the fault cleared, the conductor melted back into the insulation until the voltage could no longer maintain the arc. The insulation was not damaged severely. When the fault was not self-clearing, the cable became overheated and would be severely damaged unless the fault were cleared by other means. Results at high-altitude pressure were similar.

(*c*) *Phase-to-Ground Fault on Magnesium Alloy, at Sea Level and High Altitude.* Two forms of magnesium alloy were used, the first being 0.10 inch thick with a painted surface finish and the second an aircraft generator end shield.

The insulating properties of the painted surface finish on the former were sufficient to prevent an arc from striking when voltage was applied. However, when the surface finish was removed over a small area and the conductor applied there, a short circuit occurred with results similar to those for aluminum alloy both at sea-level pressure and in a rarefied atmosphere. The magnesium-alloy plate did not ignite, but burning particles were scattered explosively and continued to burn for some time.

Similar results were observed with the magnesium-alloy end shields.

In all tests on the magnesium alloys the current level was maintained at approximately 2,000 amperes.

Résumé of Results

The general phenomena of burning clear and welding as presented in the foregoing admit of further statements as follows on particular aspects of the short-circuit problem.

1. When the energy concentration at the fault is high (high current, low contact area) the short circuit is generally self-clearing. Thus, the higher the current level the greater is the likelihood that faults will burn clear. There would be less danger of a weld due to the higher contact pressure required.

2. Fault damage to the aircraft structure was similar for the 28.5-volt and 120-volt d-c systems as for the 120–208-volt a-c system. However, on the a-c and 120-volt

d-c systems, in almost every case, fault current flowed when the conductor and aircraft structure came into contact. With the 28.5-volt d-c system, oxide films sometimes prevented this.

3. Because high energy concentration at a fault results in self-clearing of the latter, at a given current level, the smaller the wire the greater is the likelihood of a fault being self-clearing, and the resulting structural damage is less. All wires capable of carrying a current without conductor disintegration can produce a sustained short circuit and perhaps a weld.

4. Insulations with a low melting point tend, on short circuit, to withdraw from the short-circuit area, thus exposing more conductor and increasing the danger of prolonged short circuit. The outer cotton braid usually burns to cause a distinct fire hazard. With higher melting point insulations, the binding material may disintegrate without damage to the insulation, but the latter generally has low resistance to abrasion. The thickness of the insulation used is sufficient to prevent initiation of a short circuit.

5. In the range of material thicknesses covered by the tests no appreciable difference in reaction to short circuit was noticed. However, as the thickness of the structural material increases, when a short circuit is applied, a pool of molten material is more readily formed, and thus the danger of a weld is increased.

6. At pressures corresponding to higher altitudes structural perforations are more clean-cut because of decreased oxidation. On the 28.5-volt d-c system this latter effect will increase the possibility of short circuit from an accidental contact (see paragraph 2).

7. With a light-pressure type of contact (small contact area), a fault is self-clearing for any placement of the conductor. Thus the initial type of contact between the aircraft structure and the conductor, except as regards pressure, is relatively unimportant.

8. Vibration of the airplane structure increases the fault duration and the resulting damage. An accidental contact is likely to produce a fault at any voltage because of abrasion of the contact surfaces (see paragraph 2), and the arc may be re-established after clearing.

9. Of the materials tested, fire danger due to the scattering of molten metal particles was greater with magnesium because of the tendency of the particles to continue burning. No other significant difference was noted between the aluminum and magnesium alloys using copper-wire conductors.

Conclusions

The experimental work showed that the damage to the aircraft structure depended upon the current and the current density at the fault. In all cases when the contact area between the cable and airplane structure was small the latter was perforated and the fault self-clearing irrespective of the ampere capacity of the systems tested. Although in the majority of cases the extent of the perforation was insufficient to weaken the airplane members, similar short circuits occurring on the oil or gasoline lines would probably result in serious damage from fire. Fire may also occur if molten aluminum-alloy particles, scattered as a result of a short circuit as previously mentioned, should fall on inflammable material within the airplane.

Figure 9. Damage to aircraft structure and cable from 50-caliber-machine-gun bullets

Range: 80 yards. Resulting electric-fault damage shown at *A* where weld took place

When the pressure between the conductor and airplane structure was increased, and the contact area thus enlarged, the short circuit was continuous, and at times a weld was produced. The pressure required for such a sustained short circuit was dependent upon the ampere capacity of the electic systems. From this type of short circuit serious damage to the generating system is conceivable unless the fault is cleared by suitable circuit protective apparatus.

On the 28.5-volt d-c system, of a total of 70 contacts applied with small contact area, all resulted in the fault being self-clearing; of a total of 30 contacts applied with increased pressure, 26 resulted in a weld or a continuance of the short circuit without arc or weld, while the remaining four burned clear. The division of results on the a-c and 120-volt d-c systems was somewhat the same. In the high-altitude-pressure tests the results were practically the same as obtained at sea-level pressure. With a light contact at the short-circuit area the fault burned clear and, because of less oxidation, the resulting perforation was somewhat more clean-cut than that produced at atmospheric pressure. Increase in contact pressure resulted in sustained short circuits as before.

Vibration of the aircraft structure increases the duration of a fault and the resulting damage to the aircraft structure. When only four generators formed the 28.5-volt d-c power system, the pressure required on the conductor to produce a weld was much less than when one, two,

Electrically Driven Gyroscopes for Aircraft

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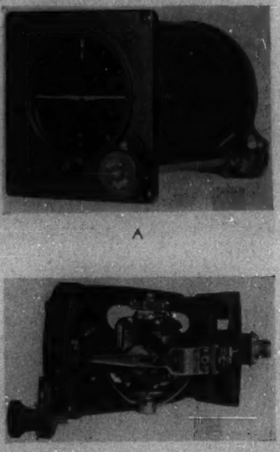
GYROSCOPIC instruments are given a place of prominence on the instrument panels of military and commercial aircraft. They are among the "most used" of all the instruments and are necessary for blind flight since they serve as stable references in space when natural objects are not visible. This group consists of a gyro horizon, showing the attitude of the aircraft—that is, whether or not it is in level flight; a directional gyro, which shows departure from course; and a bank and turn indicator, indicating rate of turn.

Operating Principles of Gyroscopic Instruments

Regardless of the type of power used, the gyroscopic principle and general construction of these instruments are as outlined hereinafter.

The gyro horizon (Figure 1) consists of a spinning rotor with its shaft mounted vertically in a gimbal which is pivoted athwartships to another gimbal. This outer gimbal is in turn pivoted fore and aft to the main frame of the instrument. The axis of the rotor is mechanically free to tilt in any direction with respect to the instrument case and by means of a bar and pointer relative motion of the rotor axis, and the case is indicated on a dial. Since the spinning rotor tends to stay vertical, the bar and pointer show movement of the instrument case, or the aircraft, with respect to a fixed vertical.



A

B

Figure 1. Gyro horizon indicator

In the side view the cover and caging mechanism are removed to show the gyroscope

The significance of the position of the bar and pointer is shown in Figure 2.

In the directional gyroscope, the spin axis of the rotor is horizontal. The motor frame, or gimbal, is pivoted horizontally to an outer gimbal, and this is pivoted vertically to the main frame. A gear attached to the vertical gimbal drives a dial, calibrated in degrees, through a 1 to 1 ratio, and since the spinning rotor and its gimbal tend to stay in a fixed position in space, movement of the instrument case (or of the aircraft) around its vertical axis will cause rotation of the dial (see Figure 3). Any departure of the plane from a fixed course will be indicated by movement of the dial with respect to its index.

The gyroscope of the bank and turn indicator consists of a spinning rotor with its shaft athwartships and its bearings in a frame or gimbal which in turn is pivoted fore and aft to the instrument frame. The gimbal and rotor assembly is restrained by a spring and a pointer is mechanically linked to the gimbal to show direction and magnitude of its movement (see Figure 4).

The spinning rotor tends to maintain its position in space, but if torque is applied in the plane of the rotor axis precession will occur, that is, the rotor will tilt in a plane 90 degrees from that of the applied torque. The tilt will be in the direction to make the spin axis of the rotor coincide with the axis of the applied torque. Turning of the aeroplane thus causes precession, and movement of the pointer shows direction and rate of turn (degrees per minute). A ball bank indicator, consisting of a ball in a liquid filled curved glass tube, is set into the face of the instrument.

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or three batteries were added to the circuit, the pressure difference increasing successively with each addition. With the generators operating under load conditions the fault short-circuits most of the load and the burning-clear phenomenon is, in general, as before. The effect of increasing the short-circuit resistance is similar to reducing the ampere capacity of the electrical system and the contact pressure necessary for producing a sustained short circuit is reduced. Thus faults occurring in sections of the circuit remote from the generating system will require less pressure to produce a weld than those occurring near a bus. Although an increase in system ampere capacity reduces the hazard from a weld-in type of short circuit, this may not be the case for short circuits which are self-clearing. In these cases the danger from fire will be greater and the short-circuit damage to a highly stressed member may be such as to cause collapse of that member.

The minimum current, of the series of d-c tests, causing damage to an airplane section was 1,100 amperes, the fault clearing after 0.74 second. The minimum time for damage was 0.05 second with a maximum peak current value of 3,420 amperes. Current values ranged as high as 4,500 amperes, and some short circuits carrying such currents did not burn clear until after two seconds' duration.

In the a-c tests, the current level was limited, and for a maximum value of 2,150 amperes the fault cleared in 0.035 second. This was also the minimum time of short circuit for a size 1/0 cable which was the type used throughout the d-c tests.

References

1. Electric-Circuit Fault-Protective Principles as Applied to D-C Aircraft Systems, R. H. Kaufmann. AIEE Transactions, volume 63, 1944.

2. Quicker Starting Automatic Oscillograph, C. M. Hathaway. *General Electric Review*, volume 40, February 1937, pages 98–100.

Operational Requirements

Instruments, such as described previously, have been in common use for many years. These have been air driven and have performed quite satisfactorily under usual peacetime flight conditions. Limitations of air operation at extreme altitudes and temperatures experienced in combat can be removed, and other advantages obtained, by electric drive. The trend toward complete electrification of aircraft also makes electrification of these instruments desirable.

The general requirements for such instruments are as follows:

1. Reliable and accurate performance from plus 70 to minus 54 degrees centigrade and at all altitudes to 40,000 feet.
2. Quick starting.
3. Operation in excess of 1,000 hours without servicing.
4. Small size and light weight.
5. Accessibility of parts for servicing.
6. Protection from dust and harmful fumes.
7. Ability to withstand vibration and shock.

Power Supply

The selection of the type of power to be used for electrically driven gyroscopes is influenced by factors of convenience and performance. Low-voltage direct current is universally available on aircraft and would be desirable from the standpoint of application. However, the problems of commutation at high speeds and high altitudes make it impractical for sensitive instruments.

The gyro horizon and the directional gyro are essentially free gyroscopes, very sensitive to balance and bearing friction. High speed is essential for stability, and the products of brush and commutator wear would rapidly affect the performance of these devices and make frequent cleaning and servicing necessary.

A 115-volt three-phase 400-cycle power supply was chosen for these instruments since this is now available on many planes or can be made available by the installation of an inverter. Polyphase is preferable to single phase from the standpoint of simplicity of motor design and starting characteristics.

The bank and turn indicator does not require the same degree of refinement mechanically or gyroscopically as the other instruments, since it is not a free gyro and indicates short-time departures from normal. A 5,000 rpm centrifugally regulated direct-current motor is satisfactory for this device. The design details of the bank and turn indicator are not within the scope of this paper, which deals primarily with the application of electricity to the operation of the gyro horizon and the directional gyro.

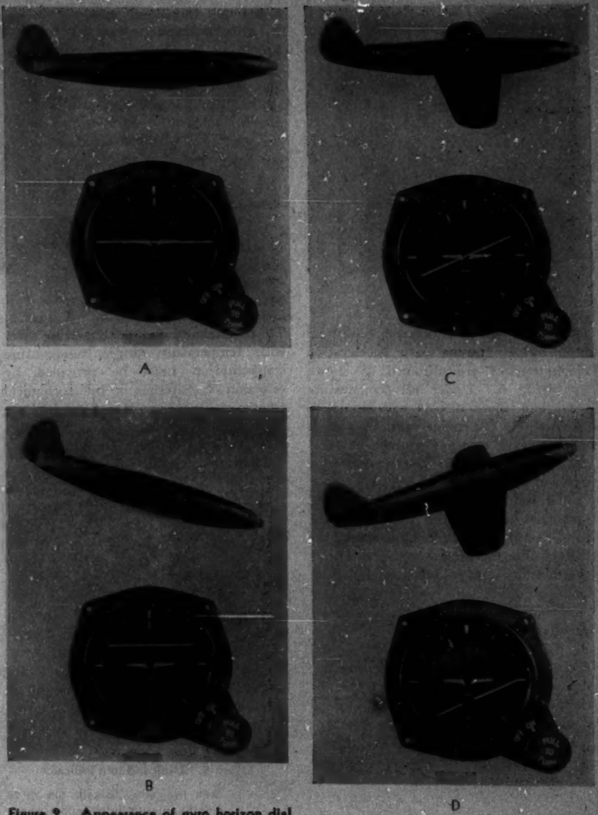


Figure 2. Appearance of gyro horizon dial for various attitudes of the aircraft

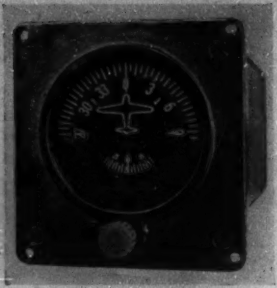
The wings represent the plane on which the instrument is installed, the bar the horizon

A-C Motor Design

Any gyroscope has rigidity by virtue of its rotor speed and the radius of gyration of its spinning mass, the latter factor being a squared function. Since dimensional and weight limitation of the instruments fix the weight of the rotor at approximately one pound, it is necessary to use as high a speed as possible consistent with long life, and to concentrate the weight at its outermost diameter. This latter requirement led to an umbrella-style rotor with the driving element within.

A hysteresis type of motor was chosen since it has good starting characteristics, runs at synchronous speed, and makes possible a symmetrical rotor with all smooth surfaces which can be manufactured with inherently good balance. The rotor consists of a bronze cup with rings of magnet steel spun in and ground internally for concentricity and accurate diameter. The rotor assembly must be precisely balanced for good instrument operation as well as life.

The stator is wound for four poles,



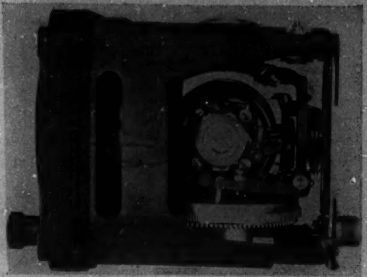


Figure 3. Directional gyro indicator

In the side view the cover is removed to show the gyroscope

Figure 3A is shown at the left, 3B at the right

fixing the speed at 12,000 rpm. With an input of less than ten watts, acceleration characteristics are obtained as shown in Figure 5. Under all temperature conditions these motors will be up to speed within the engine warm-up period, thus assuring safe performance at take-off. Figure 6 shows the speed-torque curve of the motor.

The useful life of a gyroscopic instrument has usually been limited by the rotor bearings. In addition to speed, the factors affecting life are unit pressure on the bearing surfaces, lubrication, dimensional accuracy, and cleanliness.

A cross-sectional view of the bearing developed for the gyro motor is shown in Figure 7. The unit pressures on the shaft pivot are kept at a minimum by forming it to a radius just slightly greater than the radius of the ball. The ball race is ground and lapped to a spherical form so that the ball track remains circular even though the center lines of the race and shaft do not exactly coincide. The contour and finish of the ball track in both the shaft and race are manufactured to exacting standards. Balls are matched for diameter and checked for sphericity within 25 millionths of an inch. Parts are cleaned and assembled in an air-conditioned area to insure absolute cleanliness.

The high torque of the motor makes it possible to use a generous amount of high quality lubricant in each bearing with reservoir pads back of the ball race. With electric drive, the instrument case may be sealed against dust and fumes which might contaminate the bearing lubricant. There is no flow of air through the case to pull out the lubricant at high temperatures. Life tests indicate well over 1,000 hours' operation without servicing.

Bearing adjustment is extremely critical, and for good operation cannot vary more than 0.0001 inch from optimum. To maintain this precise setting over wide temperature ranges, temperature compensation must be provided by selection of materials or by mechanical compensating means. Good results have been obtained by matching the expansion characteristics of the materials used for the shaft and gimbal.

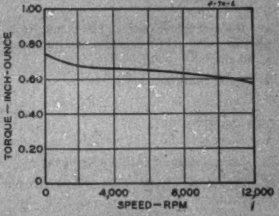


Figure 6. Torque-speed curve, 115-volt 400-cycle three-phase motor for gyroscopic instruments

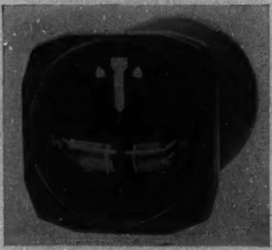


Figure 4. Bank and turn indicator

The ball bank indicator beneath the gyro-actuated pointer serves to indicate the proper angle of bank for any rate of turn

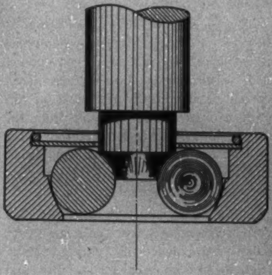


Figure 7. Cross section of bearing for gyro motor

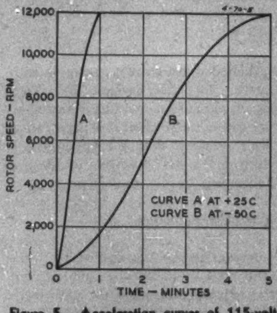


Figure 5. Acceleration curves of 115-volt 400-cycle three-phase motor for gyroscopic instruments

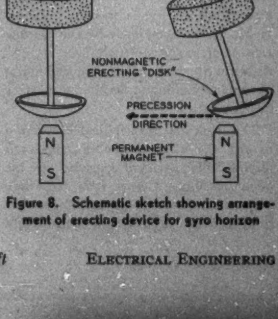


Figure 8. Schematic sketch showing arrangement of erecting device for gyro horizon

Gimbal Bearings

The requirements for gimbal bearings are just as exacting as for motor bearings but with more emphasis on sensitivity and less on ability to stand up under severe service. The bearing commonly used in existing gyroscopic instruments was found to be satisfactory for this application.

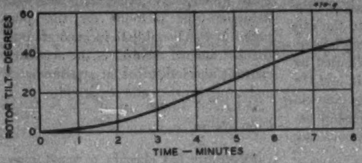
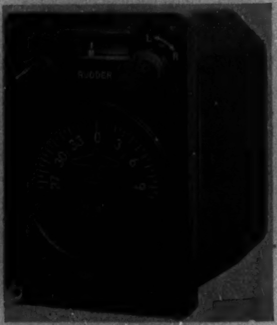


Figure 9. Erection characteristics of gyro horizon

Various tilt angles up to 45 degrees have been plotted against the time in which the erection device returns the axis of rotation to a true vertical

Accuracy of dimension and cleanliness are just as critical as for the motor bearing, and alignment of bearing race and pivot must be held to extremely close tolerances.

In the assembly of the motor and gimbals, each unit subassembly must be accurately balanced. Unbalanced torque about any axis will cause error in indication. Bearings must be set with no end shake to avoid shifting weight. Materials must be selected to avoid change in bearing adjustment or shifting of the center of gravity of any subassembly with changing temperature.



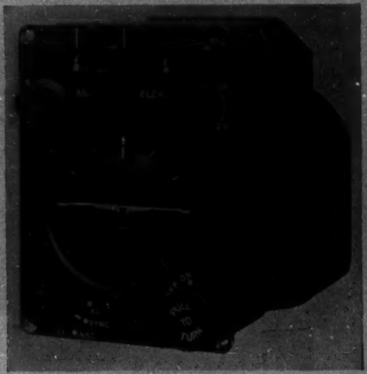


Figure 10. Directional gyro and gyro horizon equipped with pick-offs, potentiometer controls, and synchronizing indicators to serve as the "brains" of an automatic pilot

Figure 10A is shown at the left, 10B at the right

Coupled with the necessity for no end shake of bearings is the equally important requirement of minimum friction. Friction in the gimbal bearings causes inconsistency in the indication of the gyro horizon and drift in the directional gyro.

Power Leadins

The method of getting power to the motor through the gimbals presented some difficulties, since any extraneous torques or frictions are detrimental to the functioning of the instrument. Movable contacts add friction of too great magnitude and the probability of variable friction. Flexible leads or pigtails introduce variable torques or forces as well as insulation hazards. Small instrument-type spirals, while not ideal from the standpoint of space requirements, have low uniform torque, no friction, and can be well insulated. These have proved to be quite satisfactory, and by assembling them to have zero torque at the normal operating positions their effect on the accuracy of the instruments is negligible.

Slip rings and brushes are used on the vertical gimbal of the directional gyro, since it must have unlimited freedom of movement on this axis. The effects of friction on this axis will usually balance out so long as it is uniform and equal in both directions.

Gyro Horizon-Erection System

Since the motor shaft of the gyro horizon is normally vertical, gravity may be utilized to apply a correcting force when it is not in a vertical position. Such an erecting device serves to maintain the rotor in a vertical position over long periods of time and to bring it back to vertical should it be upset or started in a nonvertical position.

Erection in air-driven instruments is accomplished by means of gravity-actuated valves controlling four air streams which supply the correcting forces. This method is, however, subject to decreased effectiveness with increased altitude and would require additional auxiliary equipment if applied to the electrically operated devices. A unique application of permanent magnetism, therefore, has been developed to accomplish this function without the use of auxiliaries.

This erecting device consists of a pendulum free in two axes and carrying a small permanent magnet in close relation with a copper-alloy disk attached to the lower end of the motor shaft. When the motor shaft is vertical, the magnet, under normal conditions, is suspended at the center of the spinning disk, and no unbalanced force results from the damping effect of the magnet on the disk (see Figure 8). However, if the rotor is tilted from vertical, the magnet exerts a force on the spinning disk in a direction to cause precession toward the true vertical.

The rate of correction is made low enough, so that random accelerations of the aircraft are averaged out in normal flight. In turns, however, the angular acceleration causes the magnet to swing out and pull the rotor off vertical. No simple way has been found to eliminate this turn error due to the erecting device, but compensation can be made for 180-degree turns at one rate of turn. Practice has established 180 degrees per minute as the standard rate, and compensation is accomplished by tilting the motor shaft forward a few degrees from vertical, the exact angle being determined by the erection rate.

Use and Characteristics of the Directional Gyro

The directional gyro and magnetic compass are supplementary instruments in that the compass indicates direction but is subject to turning errors and instability in rough air, while the directional gyro maintains a set course in a deadbeat manner. In practice, the directional gyro is used to hold course, but it is periodically checked against the magnetic compass and reset as required.

Since the gyroscope tends to stay in the position set, the perfect instrument will show drift because of the rotation of the earth. The magnitude of this drift varies with latitude. It would be zero at the equator where the rotor shaft is parallel to the earth's axis, and 15 degrees per hour at the poles where the rotor shaft is 90 degrees from the axis of the earth. Correction can be made for a given latitude, as for example 45 degrees north, but the error is then doubled at 45 degrees south latitude. Correction is made by moving a small adjusting weight, and it is not difficult to change the setting to obtain compensation for the latitude in which the instrument is to be used.

The directional gyro is equipped with an antitumbling device which maintains the gyro motor in a perpendicular relationship with the vertical gimbal so as to set up a maximum of safe operating range available for maneuvering of the aircraft. In the electrical design, a simple means is employed. Gyroscopic theory dictates that torque about one axis of a gyro normally produces precession about a perpendicular axis; however, if that precession is resisted, the precession is reflected back into the axis initiating the torque. By properly balancing the centering torque contributed by the spirals about the motor axis, and the counterfriction torque about the vertical axis, the perpendicular relationship between the motor and vertical gimbal may be maintained.

Caging Mechanism

Both the horizon gyro and directional gyro are equipped with caging mechanisms to hold the delicate gyro elements to prevent damage resulting from acrobatics of the aircraft beyond the limits of freedom of the devices. The caging mechanisms also allow the pilot to reset the instrument to its normal operating position. In the case of the gyro horizon, the pilot may level his aircraft and uncage instrument, saving the erection time necessary for it to restore itself. In the case of the directional, the pilot, by caging and turning the caging knob, may rotate the gyro element to any position of reference he desires.

Adaptability to Control

Electrically driven gyros lend themselves conveniently to interconnection for automatic control such as required in the automatic pilot. Figure 10 illustrates a gyro horizon and a directional gyro which have been equipped with induction-type pick-offs for this purpose. It was possible to incorporate into the instruments themselves the necessary controls and indicators for synchronizing and adjusting trim and sensitivity.

Conclusion

Electricity has brought to small gyroscopic instruments increased reliability and improved accuracy under extremes of temperature and altitude. It permits sealing of the case to protect the sensitive mechanism from dirt and corrosive fumes and thus prolongs the life of the instrument by preventing bearing wear due to grit or loss of lubrication. The ability to remove the cover with the operating mechanism intact aids in locating trouble and simplifies overhaul and adjustment.

The present trend is toward complete electrification of aircraft, eliminating, to a large extent, the pneumatic and hydraulic systems now commonly used. Electrification of the gyroscopic instruments is a necessary part of this program.

Transmission and Relaying Problems on the Fort Peck Project

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THE recently completed Fort Peck power plant with its substation and transmission facilities has several features which are unique and of particular interest. This paper will point out application problems and their solution in connection with power transmission and relaying.

The initial installation of generation at Fort Peck consists of one 38,888-kva 13.8-kv 128.5-rpm three-phase 60-cycle water-wheel generator. Ultimately, two more generators are to be installed, one a duplicate of this first unit and one smaller unit. The substation is located on the west bank adjacent to the powerhouse and at present has one large transformer bank consisting of three 16,667-kva single-phase transformers stepping up to 161 kv from generator voltage. A 15,000-kva three-phase shunt reactor is installed on the 13.8-kv bus just ahead of the power transformers. All switching, except for transmission-line disconnects, is performed at generator voltage, and all power circuit breakers are of the compressed-air type.

The only high-voltage transmission associated with this initial installation is the one 161-kv line extending to the Rainbow substation bus of the Montana Power Company at Great Falls, Mont. The total length of the line is 288 miles; it is of H-frame construction, and the conductor employed is about half 477,000-circular-mil steel-reinforced aluminum cable and about half 300,000-circular-mil copper. The line was built originally to feed power to the dam site from Great Falls for use during construction.

At the Montana Power Company, terminal of the 161-kv line, a bank of three 20,000-kva single-phase three-winding autotransformers step down to 105 kv for connection onto the Rainbow bus at that voltage. At this location switching is performed at 105 kv. The power transformers at both ends of the line have the neutral solidly grounded on the high-voltage winding.

Figure 1 shows a simplified one-line diagram of the Fort Peck installation, transmission line, and receiver system. This sketch includes the system elements which will be discussed specifically in subsequent paragraphs, and most of the equipment shown has been described previously.

The unusual length of the transmission line involved introduces interesting application problems. This line length, coupled with hydrogeneration, immediately suggests a study of transient-stability limits for faults occurring on the Montana Power Company system at or near the Rainbow substation bus. Because of the very great shunt capacitance inherent to a single line section of nearly 300 miles, problems concerning open-circuit voltages and synchronizing are of importance. Also, because of line length, relaying presents a special set of conditions which standard high-speed impedance relays cannot meet. The salient factors affecting these problems and the measures considered in reaching the solutions will be examined.

General-System Studies

The a-c network calculator was employed in studying the general system shown in Figure 1. The relative simplicity would permit manual calculations with reasonable accuracy, but the use of the calculator permitted complete study, including numerous points too time-consuming by manual methods.

Four variations of the system finally adopted were studied, and these four connections are shown in their bare essentials in Figure 2. Note that the principal difference lies in transformers and shunt-reactor application. Henceforth, these four systems will be referred to as systems *a*, *b*, *c*, and *d*.

Of interest is the fact that all four of these systems operate satisfactorily in the steady-state condition, that is, from a standpoint of load and voltage regulation. The differences appearing in voltage levels at the two load busses in the different systems always can be compensated adequately by control of excitation at the two terminals. The resultant exchange of reactive is sometimes objectionable for other reasons, but in the steady-state case it is always operative. The principal reason for considering the unusual voltage ratios of systems *c* and *d* that result in a "sloping" voltage characteristic was to study the possible advantage of accepting excessive line drop in a very long line under load and adjusting normal transformer ratios a full-voltage class to meet it. The inherent decrease in line-charging kilovolt-amperes is a further advantage. Generally, the disadvantages accruing from reduced voltage level overweighed advantages in the cases studied. These relative merits will be discussed more specifically in following paragraphs.

Synchronizing

The charging kilovolt-amperes of the 288-mile transmission line, when operating at 161-kv, is approximately 43,000 kva. Since the generator rating at Fort Peck is 38,888 kva, it is impossible to charge the line at normal voltage in the connection of system *a*, unless part or all of the charging capacity is supplied from Rainbow. This presents an operating inflexibility, since it is impossible to synchronize at normal voltage at Rainbow.

By adding the 15,000-kva shunt reactor and obtaining system *b*, sufficient

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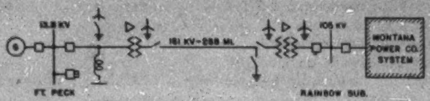

Figure 1. Simplified one-line diagram of Fort Peck system connection

line charging is canceled to permit the Fort Peck generator to "pick up" the line, and synchronizing can be accomplished at either terminal. Furthermore, when the line is energized from Rainbow, this shunt reactance cancels almost all of the voltage rise inherent to the line, so that only about three per cent differential exists across the synchronizing breaker (31 per cent for system *a*).

The sloping line-voltage characteristic of system *c* inherently reduces charging current to a figure of 38,000 kva but still does not permit synchronizing at Rainbow, since the generator charging capacity does not equal this figure, and because a voltage differential of 50 per cent exists across the synchronizing breaker at Rainbow. Such a voltage differential might be objectionable because of the large surge of reactive current that would occur immediately subsequent to closing the breaker. The sloping voltage characteristic presents ideal synchronizing conditions across the open switch at Fort Peck if the line is charged from Rainbow. The rising voltage characteristic very nearly follows the intentional "uphill slope," so that only seven per cent voltage differential from normal exists, a figure that could be met easily by generator regulation or be ignored altogether.

System *d* presents practically the same case as system *c* with respect to synchronizing, except that the line charging is further reduced by reduced voltage to a figure of 23,000 kva. The Fort Peck generator easily can supply this quantity, but the voltage differential across the open switch at Rainbow is then 66 per cent, an excessive figure.

System *b*, comprising full 161-kv voltage operation with a 15,000-kva shunt reactor, presents the most flexible case for system interconnection initiated at either terminal.

Stability

Since only a single-circuit line interconnects Fort Peck with the Montana Power Company, the tie is severed subsequent to each transmission-line fault, and there is no transient-stability limit. Transient stability becomes an important consideration, however, for faults and switching on the Montana Power Company system. Numerous high-voltage circuits emanate from the vicinity of the Rainbow bus, and, if Fort Peck were to swing out of step for faults or switching on these circuits, the effectiveness of the interconnection would be impaired seriously. For this reason stability was studied for faults on this adjoining system.

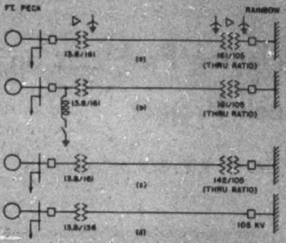

Figure 2. Variations of system studied

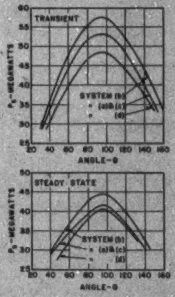

Figure 3. Power-angle curves for the systems studied

P_e = power output—Fort Peck generator

θ = angle between internal voltages

Power-angle curves applying to the different systems studied are shown in Figure 3. Note that system *b* offers a substantial increase in stability over the other systems studied, as demonstrated by the power-angle relations. All systems maintain stability for switching operations on the Montana system, and all systems are stable for single line-to-ground faults occurring near the Rainbow bus and cleared in relaying and breaker times actually obtaining on the Montana systems. System *d*, employing lower-voltage transmission, presents a marginal case for single line-to-ground faults, and this was one of the considerations that led to its discard. None of the systems studied are stable for multiphase faults near the Rainbow bus, although angle–time curves indicate that system *b* may provide a stable solution for many multiphase faults that occur some appreciable distance beyond the interconnecting point.

Of unusual interest is the fact that systems *a* and *c* show identical power-angle characteristics. This is a function of the large line-charging kilovolt-amperes in comparison with total generation that exists in this installation. If the voltage is lowered somewhat, line charging is reduced enough to permit the Fort Peck generator to operate at a lagging power factor and, of course, at a high internal voltage. This increase in internal voltage completely offsets the increased equivalent line impedance inherent to the lower-voltage operation.

The increase in stability with system *b* is due solely to the 15,000-kva shunt reactor which permits generator operation in the lagging range. The power-angle curves for the steady-state case (Figure 3) show that system *b* will be operating at 79 per cent of the steady-state limit when full-load kilowatts (35,000) are generated. It was believed that operation at above 85 per cent of this limit was likely to be unsafe during system disturbances, and this is borne out by the transient solution that indicates system *d* to present marginal stability for single line-to-ground faults beyond Rainbow. Systems *a* and *c* eliminate this uncertainty, but the considerable extra margin provided by system *b* for relatively small investment was believed good insurance.

Water Rheostat

The water rheostat was necessitated by the omission of the surge tanks because of wartime conditions. The origi-

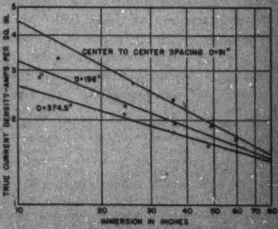

Figure 4. Results of field tests on water rheostats

Four-inch outside-diameter electrodes at 13.8 kv nal plans provided for installation of three surge tanks of adequate size to provide satisfactory hydraulic stability and capacity for the ultimate installation of three units, with each surge tank connected to its individual penstock by a suitable riser. The surge rise and water-hammer pressure increases were computed on the basis of using a five-second governor time for full-opening and closing the turbine wicket gates. However, it was deemed inadvisable to use large quantities of critical steel plate for this project just after the entry of the United States into the war, and the contract, therefore, was canceled.

ACTUAL WAR INSTALLATION

The completion of part of the power development was authorized subsequently, with the surge tanks omitted.

The omission of the surge tank for the installed unit developed the following conditions:

1. The governor time would have to be slowed down materially to avoid subjecting the tunnel, penstock, and turbine to excessive water-hammer pressures.
2. Involuntary rejection of line load due to line faults, or other reasons, would result in runaway of the unit unless measures were taken to avoid this.

Various schemes were investigated then with the view of securing smooth transition from steady line-load conditions to steady local-load.

The most promising of these schemes appeared to be the use of a water rheostat to replace temporarily the line load with an automatic arrangement for reducing the rheostat load by withdrawing the electrodes from the water, so that the unit could be transferred smoothly from the more or less heavy line load to the light local load. A detailed investigation of this procedure convinced the authors that such operation might result in hydraulic instability, taking the form of a series of governor swings of increasing intensity with unpredictable results.

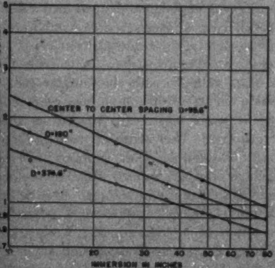

Figure 5. Results of field tests on water rheostats

$8^{5}/_{8}$-inch outside-diameter electrodes at 13.8 kv

We, therefore, suggested that, on the occurrence of a line fault and loss of line load, the unit be brought down automatically to the speed–no-load position and that the water rheostat be set at a fixed value and used only as a brake to avoid runaway speeds. A tank rheostat was installed in the powerhouse to provide for the suggested service.

The following provisions were planned to avoid runaway conditions as far as possible:

1. The line-breaker tripping relay was arranged to energize the rheostat-breaker closing mechanism and also to trip the governor speed–no-load solenoid, so that the governor would act to reduce the gate position to approximately speed-no-load position irrespective of speed changes. The rheostat load would be tripped off then by a suitable mechanism, the various adjustments being made in such a way that the unit would continue to run after the gate movement was completed but at speed approximately normal.
2. The electrical arrangement was such that the line breaker would open and the rheostat breaker would close on either of the following eventualities:
   - (*a*). Fault on the line.
   - (*b*). Opening of breaker at receiving end of line.
3. An electrical fault in the generator itself results in the field being opened and precludes the possibility of placing an electric load on the machine. In such case, the generator differential relays will act to trip the governor shutdown solenoid, causing the gates to close to the fully closed position. There is no method of avoiding a runaway speed under these conditions.

WATER-RHEOSTAT TESTS

As stated previously, the water-rheostat tank in the powerhouse was designed for braking service only and was not suited, therefore, for carrying current continuously for testing purposes. To obtain information on the effect of variations in electrode spacing, size, and immersion, as well as for applying a steady load on the turbine during certain hydraulic tests, a temporary rheostat was rigged in the tailrace. The electrode size could be varied from $2^{3}/_{8}$ to 8 inches outside diameter, the immersion from zero to six feet, and the spacing from about 7 to about 30 feet. The voltage also could be varied from zero to 13.8 kv. The resistivity of the water was approximately 590 ohms per inch cube at 15 degrees centigrade and was found to vary with temperature according to the following formula:

$$\rho=\frac{820}{10^{0.0091t}}$$

where

ρ = resistivity in ohms per inch cube
t = temperature in degrees centigrade

The test results were reduced to terms of current density in amperes per square inch, as it was realized that limitation of current density was an important factor in successful operation. A few tests were made with twin electrodes, but it was soon apparent that the behavior of these was exceedingly complex, and the majority of tests was made with 4-inch and $8^{5}/_{8}$-inch outside-diameter electrodes

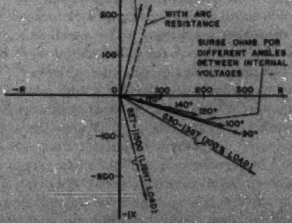

Figure 6. Impedance values as seen by relays at Fort Peck

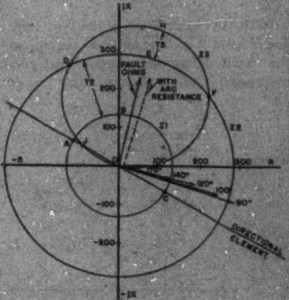

Figure 7. Distance-relay characteristics at Fort Peck

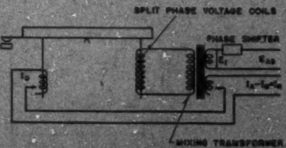

Figure 8. Schematic diagram of beam impedance element with modified characteristic

with one test of $2^3/_8$-inch electrodes. Results of tests on two electrode sizes are shown in Figures 4 and 5. The following deductions were made from the tests:

1. For immersions of 24 inches or more current density increased directly with voltage. For immersions of 12 inches or less current density increased as a power of voltage greater than unity.
2. For a given spacing (within the range tested) current density decreases as immersion increases.
3. For a given immersion (within the range tested) current density decreases as spacing increases.

Several tests were made by rejecting various line loads to various rheostat loads, and it was found that a suitable selection of rheostat loads could be made which would result in reasonable pressure changes in the penstock and reasonable speed changes of the machine, so that the test showed that the wooden tank in the powerhouse, when provided with the proper electrodes, will prove adequate for rejection at periodic intervals of loads of 15,000 kw and higher with the operating procedure as recommended. It was found impractical to build a water rheostat of large enough proportion that would be suitable for continuous operation or to be used for loads smaller than 15,000 kw.

Transmission-Line Relaying

With the construction of the Fort Peck line in 1934 it became evident, because of the small difference between load currents and fault currents, that proper protection could be provided only by zone-type distance relays, and, therefore, impedance-type relays were selected. Whereas there was little difference between the load-current and fault-current magnitudes, there was a wide difference in their phase-angle positions. Accordingly, the phase-angle characteristic of a standard impedance relay was accentuated and shifted to the proper operating zone by manipulation of the capacitor potential devices, and these relays have given very satisfactory service for several years. It is believed that this was the first instance of the application of a phase-angle discriminating feature to a distance relay in order to cope with the conditions on an extremely long transmission line. However, with the addition of the generating equipment at Fort Peck, the conditions with which the distance relay must cope have changed, and it has become desirable to extend further the phase-angle discriminating capacity of the impedance relays.

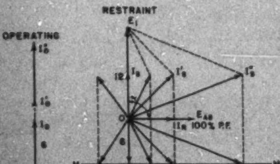

Figure 9. Vector diagram of current and voltage forces acting on the beam

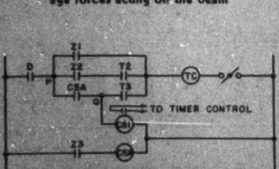

Figure 10. Schematic trip-circuit connection

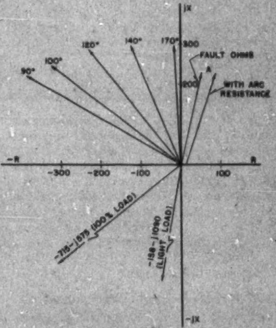

Figure 11. Impedance values as seen by relays at Rainbow

The new conditions presented to the relays are the presence of synchronizing surges on which relay action is not desired until the surges approach actual out-of-step conditions. The conditions, as an impedance relay located at Fort Peck sees them, are shown in Figure 6 which gives the magnitude and phase angle of the impedance ohms which are presented to the relays. The impedance vectors, designated by angles from 90 to 170 degrees, represent the ohm values as seen by an impedance relay obtaining its current and voltage from the 161-kv line at Fort Peck for different angles between the internal voltages at Fort Peck and Rainbow, as indicated. It may be noted that the maximum load-ohm vector is considerably longer than both the fault-ohm vector and the surge-ohm vectors, and, therefore, a standard impedance relay could be set to trip for faults but not for load currents. However, it has been determined that the system can experience surges up to 130 degrees between internal voltages and still recover synchronism after an external fault is cleared. If the internal voltages at Fort Peck and Rainbow deviate more than 130 degrees, actual out-of-step conditions are reached, and the line must be opened. It is clear from Figure 6 that a standard impedance relay, when set to trip for faults at Rainbow, will trip also for surge conditions from which the system can recover, since the surge-ohm values are too close to the fault-ohm values.

It was desired to have at Fort Peck a relay characteristic which would provide instantaneous tripping for faults over a portion of the line, time-delay tripping for faults over the remainder of the line, with time-delay backup tripping for faults beyond the Rainbow terminal, and instantaneous tripping on swings beyond a 130-degree angle between internal voltages. This tripping characteristic is depicted in Figure 7. Instantaneous tripping is provided by the impedance element *Z*1, and the directional element over the area bounded by the points *A*, *B*, 140 degrees, *C*, and 0. A second impedance element trips in the area bounded by the points *O*, *J*, *D*, *E*, and *F*. This second element trips through a timer, the operation of which is controlled by the directional element and a third element *Z*3. The *Z*3 element is a modified impedance element which operates for any impedance vector that terminates inside the circle *O*, *J*, *D*, *H*, *F*.

To change the characteristic of a beam-type impedance element into a displaced circular characteristic, as shown, no change in the beam element itself is required. Figure 8 shows schematically a beam element with the familiar split-phase voltage restraint on the rear end of the beam and a current coil on the front end. The restraint end of the beam, instead of being supplied with a voltage only, is supplied with the vector sum of a current and voltage. The delta current I_A–I_B is supplied to a primary winding of a transformer, while the voltage E_{AB} is supplied through a phase shifter to another winding. The secondary winding feeds the standard split-phase voltage coils. The current I_A–I_B also flows to a current coil on the contact end of the beam.

The action which takes place to cause the element to have a circular characteristic can be seen from the vector diagram in Figure 9. This is drawn for the case where the center of the circle is to be placed on the X axis and passed through zero. The current I_E in the transformer winding is in phase with the voltage E_{AB} at 100 per cent power factor, but the voltage E_1 applied to the other winding is shifted 90 degrees leading. The operating energy on the front end of the beam is represented by the vector I, although this is the same current as that operating on the rear end of the beam. While the current on the front end is shown as a vector, no vector relationship exists between the pull on the front end and the pull on the back end of the beam, since the beam has substantially no response to the phase angle between the forces on the two ends. It is arranged that the current I_0 produces a mechanical force on the front end of the beam equal to the force produced by the current on the back end of the beam when no voltage is present. For fault currents lagging 90 degrees it is easy to establish the conditions for a balance point. For instance, if the voltage E_1 is assumed to be 12 units, then it is evident that the current I_R directly opposite must be 6 units in order for the net restraint of 6 units just to balance an operating force of 6 units. If the current lags the voltage less than 90 degrees (say I_E'), it must be increased to a value such that the vector sum of I_E' and E_1 (or I_S') still equals I_R', because the operating current I_0 has increased at the same rate, in order to maintain a balance point. It can be seen from an inspection of Figure 9 that the current I_E and I_0 for any angle must terminate on the straight line MM in order to make the scalar value of the vector sum $E_1+I_R=I_S=I_R=I_0$. In other words, the line MM represents the locus of balance currents for a given voltage E_1. Division of the voltage E_1 by the balance currents gives impedance values, which plotted on R and X co-ordinates gives a circle with its center on the X axis and passing through zero. The center of the circle can be placed on any other axis by regulating the angle α between E_1 and E_{AB}. For instance, if α is less than 90 degrees, the circle center is at a point between the $+X$ and $-R$ axes; if α is between 90 and 180 degrees, the center is between the $+X$ and $+R$ axes.

The aforementioned element follows the equation $EI \cos(\alpha+\theta)=E^2$, and such a characteristic has been used for a number of years in connection with an induction element. It may be noted, however, from the equation that, for close in faults where E approaches zero, the energy in the element also approaches zero resulting in sluggish indecisive action. It is interesting to note that a beam element following this same law is not operating under a low-energy condition. On the contrary, since heavier currents are obtained for close-in faults on long lines, the energy in the beam is even greater when the voltage is zero. Positive high-speed operation, therefore, at zero voltage is readily obtained by very slightly increasing the current pull on the front end of the beam with respect to the current pull on the rear end.

RAINBOW RELAYS

The ohmic picture presented to the relays at Rainbow on the 105-kv bus is shown in Figure 11. Here the only requirement is that the relay characteristic distinguish the surge ohms through 130 degrees and the load ohms from the fault ohms. Figure 12 shows the characteristic chosen. Here, however, it was desired to set $Z1$ element well into the transformer bank at Fort Peck with $Z2$ set beyond the low-voltage bus and $Z3$ even farther. Because of the long setting on $Z1$ element, and also to take care of possible future increases in the capacity at Rainbow, it was considered desirable to supervise the $Z1$ element through the $Z3$ element. This was done by moving the $Z1$ trip connection from point P to point Q on the schematic diagram in Figure 10. Placing the $Z1$ element effectively in series with the $Z3$ element means that the $Z1$ element can no longer trip independently for close-in faults, and the $Z3$ element must operate positively for all faults. As mentioned previously, the beam element possesses plenty of energy for faults giving zero voltage, even though its characteristic circle passes through zero. The element, however, possesses an additional advantage in that a definite margin of safety can be attained. This is done by passing the circle beyond zero as shown by the dotted circle $Z'3$ of Figure 12. This is accomplished very easily by increasing the current force on the front end of the beam in relation to the current force on the restraint end of the beam. The amount that the zero point is projected into the circle is regulated by the excess force on the front end of the beam as compared with the current force on the back end, and is accomplished very readily by manipulation of the current taps and core screw on the front end of the beam. The diameter of the circle, or the ohm setting, is regulated directly by manipulation of the current taps on the beam and on the transformer-primary current coil in unison, keeping the relative ampere turns constant.

With only a single high-voltage line involved, no high side switching is employed, and the differential relaying on the two terminal transformer banks cannot isolate low side faults. The impedance relays described will isolate

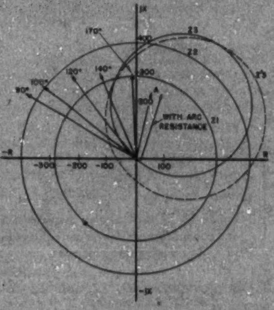

Figure 12. Distance-relay characteristics at Rainbow

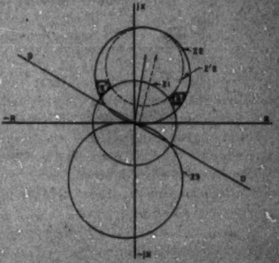

Figure 13. Distance-relay characteristics for carrier-current operation using four elements

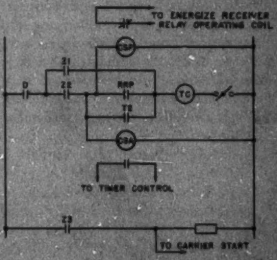

Figure 14. Schematic trip connections for relays in Figure 13

promptly in each case phase faults occurring at the far end of the line, but ground faults will not be handled if they occur on the low-voltage side of the transformers. For this reason the differential relays at each location are arranged to trip a three-phase grounding switch; an artificial phase fault is thus created; and the impedance relays clear promptly. These two grounding switches are shown in Figure 1. At Rainbow a high-voltage spring-closed switch is used, while at Fort Peck an existing 15-kv breaker is used.

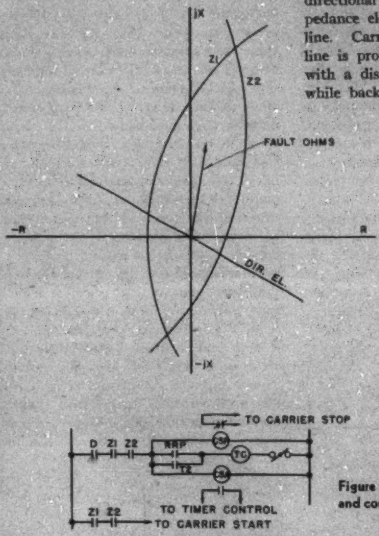

Figure 15. Distance-relay characteristics and connections for carrier-current operation using three elements

Carrier-Relay Schemes

While carrier-relay operation was not involved on the Fort Peck project, it is interesting to see how the characteristics discussed previously could be fitted into a carrier scheme. There are several ways in which the standard three-zone impedance relay can be provided with various combinations of characteristics and be used in a carrier arrangement suitable for long lines. Figures 13 and 14 show one possible combination. Carrier is started for external faults by the *Z*3 element with a displaced circular characteristic. Instantaneous tripping independent of carrier is provided by the directional element *D* and the *Z*1 impedance element over a portion of the line. Carrier tripping over the whole line is provided by *D* and *Z*2 element with a displaced circular characteristic, while backup time tripping beyond the line is provided by *D*, *Z*2, and *T*2 elements.

In some cases it may happen that the maximum swing conditions from which the system may recover involve minimum ohmic values for which tripping is not desired in the general region *S* and *S'*. The *Z*2' element can be made unresponsive to these surge-ohm values without changing its response to fault ohms by contracting the circle into the position shown by the dotted circle *Z'*2. It may be recalled that the zero point on the *R* and *X* diagram definitely can be placed inside the circle by overbalancing the current force on the front end of the beam. Conversely, the zero point definitely can be excluded from the circle by underbalancing the front-end current force. The diameter and position of the circle are regulated as before by the current taps on the operating and restraint ends of the beam. It is, therefore, evident that the circles *Z*1 and *Z'*2 can be positioned, so that tripping on severe synchronizing surges is prevented, but adequate coverage of the fault-ohm area is obtained.

Since the characteristic circle of the beam element can be adjusted to any diameter and its center placed anywhere with respect to the *R* and *X* axes, another combination is suggested using even less elements. This is depicted in Figure 15 where the circles of *Z*1 and *Z*2 overlap to provide good coverage in the range of the fault ohms, restriction in the range of load current and surges, and also coverage for external faults. Carrier is started by *Z*1 and *Z*2 and is removed by *D*, *Z*1, and *Z*2 elements which also provide a time-delay backup. Complete carrier and backup protection, as far as the distance relay is concerned, is thus accomplished with only three elements per phase.

Conclusions

The further extension of the phase-angle discriminating feature of the impedance relays, which have provided protection for the Fort Peck line since 1934, has enabled this same type of relay to accommodate the more severe conditions imposed by the addition of generating capacity at Fort Peck.

The addition of the movable circular characteristic to a beam-type impedance relay, with the easy ability to regulate the diameter and position of the circle, has opened up new possibilities in the protection of electrically long lines without the necessity of increasing the number of relay elements.

The use of shunt reactors to equalize voltages and increase transient-stability limits will increase as long line sections at very high voltages become more prevalent.

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Steady-State Stability of Synchronous Machines as Affected by Voltage-Regulator Characteristics

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STEADY-STATE stability of synchronous machines is an old subject, and the fact that voltage regulators may on occasion increase the stability limit is more or less well known.[1] The present paper, however, aims to contribute to the knowledge of the subject by:

(*a*). Presenting a method for calculating directly the gain in steady-state stability with a given regulator and excitation system and also for predetermining the optimum regulator and excitation-system characteristics to give maximum gain in stability for a given synchronous machine or set.

(*b*). Presenting the results of calculations by the methods developed for certain typical systems so as to give some notion of the possible gains under various conditions.

The relations developed may be used to study the effects of regulator and machine characteristics, power factor, regulator location, and so forth. An important application of the analysis is the case of electric ship propulsion, where the size and weight of the generator and motor are in part determined by the steady-state stability characteristics. The method of analysis consists in first setting up the transient equations of the system, then considering small displacements from any given equilibrium condition. The equations for small displacements are linear and may be tested directly for stability.

Conclusions

From the results shown here, and from additional calculations which have been made, the following conclusions may be stated:

1. A properly designed voltage-regulating system can increase the steady-state stability limit of a synchronous machine by a considerable amount.

2. Fast regulator and exciter action and low transient reactance are desirable. On the other hand, the amplification factor of the regulating system must be co-ordinated properly with the machine and system constants rather than made as large as possible.

3. The possible gain in power limit for two synchronous machines is considerably greater than for a single machine connected to an infinite bus through a tie line.

4. Amortisseur windings have little effect on the possible gain in power limit.

5. Although the exact value of exciter time constant is not important, additional lags in the regulating system should be avoided, as they reduce the gain considerably. Some exciter time lag actually may be beneficial in increasing the permissible range of regulator amplification.

6. For maximum gain in power limit, a regulator with broad regulation and rapid response should be used. A very slow droop-correcting device could be used to maintain constant voltage over a range of load.

7. A regulator stabilizing transformer has little effect on the maximum gain but can permit a much narrower voltage regulation, which may be desirable for other reasons.

8. The division of transient reactance between two otherwise similar machines (acting as motor and generator) makes little difference, but, if the field time constants of two otherwise similar machines are different, the maximum power limit is obtained if the field of the machine with the smaller field time constant is regulated.

Analysis

Preliminary Remarks

Before entering on the detailed mathematical analysis a more or less general review of the fundamental concepts involved will be given.

For a single round-rotor synchronous machine connected to an infinite bus through a tie line or for two round-rotor machines the steady-state power transfer (with zero resistance) is given by

$$P = \frac{E_1 E_2}{x_1 + x_2} \sin \delta$$

where E is a machine excitation or the voltage of the infinite bus, x is a machine synchronous reactance or the tie-line reactance, and δ is the angle between the two excitation axes.

It may be recognized that in the steady state (or generally, if the effects of rotor circuits are neglected) there is analytically no distinction between a tie line with its infinite bus and a second machine. The distinction lies rather in the values to be assigned to the various parameters and in the interpretation of the quantitative results.

For two equal machines or for a machine connected to a tie line of equal reactance with $x_1 = x_2 = 1.0$ and with the excitations set at some fixed value, for example, to give rated current at unity power factor and unit terminal voltage

$$E_1 = E_2 = \frac{1}{\cos 45°} = 1.414$$

(see Figure 6), and the power transfer is

$$P = \frac{(1.414)^2}{2} \sin \delta = \sin \delta$$

The power limit occurs at $\delta = 90$ degrees and is $P_{max} = 1.0$.

If, on the other hand, the excitations are not constant but are controlled so as to maintain unit voltage and power factor, then

$$E_1 = E_2 = \frac{1}{\cos \frac{\delta}{2}}$$

and the power transfer is

$$P = \frac{E_1 E_2}{x_1 + x_2} \sin \delta = \frac{\sin \delta}{2 \cos^2 \frac{\delta}{2}} = \tan \frac{\delta}{2}$$

Now the power transfer increases without limit as the angle δ is increased from 0 to 180 degrees. The new torque-angle curve crosses the old torque-angle curve at $\delta = 90$ degrees. The question of the meaning of this result, particularly of the possibility of stable operation in the region $\delta > 90$ degrees, now naturally arises; the answer is found in the kind of control used. If the excitations are controlled slowly, for example, by hand, so as to return the terminal voltage to unity only after a deviation is noticed, the power limit again will be found at $\delta = 90$ degrees. One may imagine the load increased in small steps. At each step the terminal voltage is returned to unity by an adjustment of the excitations, and then the system is tested for steady-state stability in the conventional way by noting whether or not a small increase in angle with fixed excitations results in an

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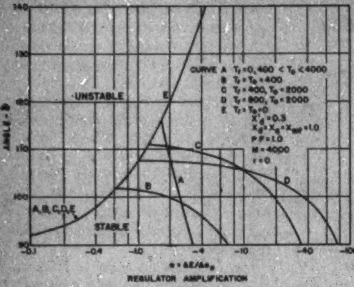

Figure 1 (left). Effect of automatic voltage regulator on steady-state stability limit

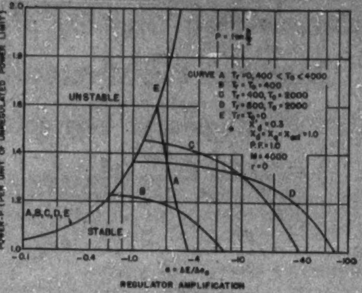

Figure 2 (right). Effect of automatic voltage regulator on steady-state stability limit

increase or decrease in power transfer. However, if the excitations are controlled automatically to maintain constant terminal voltage continuously and instantaneously, regardless of load or angle, and even during the stability test, the relation $P = \tan \delta/2$ will hold instantaneously, and there no longer is a stability limit. In order to maintain the constant terminal voltage required for such stable operation it is necessary to have a flat regulator (that is, one with an infinite amplification factor, $a = -\infty$) and no time lags in the regulator, exciter, or even in the main machine fields, so that the excitations are corrected instantaneously. Such action is, of course, practically unrealizable because of the time lags inherent in any system. Moreover, both excitations also increase without limit as the angle δ approaches 180 degrees.

It is shown in equation 33 that, for a single machine connected to an infinite bus through a tie line of reactance $x = 1.0$, instantaneous control of only one excitation is sufficient to allow stable operation at any angle up to 180 degrees as the amplification factor is increased, but the amplification factor now must be twice as large as that required if both excitations are controlled instantaneously. Of course, the conditions of unit terminal voltage and power factor imply that, in any case, the excitations or the infinite bus voltage must be adjusted (possibly by hand) to maintain these conditions. Thus, in the limit both excitations still approach infinity as before.

If the actual case considered is really a tie line to an infinite system, rather than two machines, motor and generator of equal size, it would seem more reasonable to study the condition in which the infinite bus voltage as well as the machine terminal voltage is maintained at unity, letting the terminal power factor fall where it may. Then with a tie-line reactance and machine reactance each equal to unity, the power limit unregulated can be shown to be $P_{max} = 0.866$, and "ideally" regulated (to maintain unit terminal voltage instantaneously) to be $P_{max} = 1.0$. The maximum possible improvement is seen to be very much smaller than for the two-machine case.

In order to make it easier to follow, the mathematical analysis is presented first as applied to a single machine and next, in less detail, as applied to two machines.

A. Single Machine and Tie Line. This section is concerned with the effect of a voltage regulator on the steady-state stability limit of a synchronous machine connected through an impedance tie to an infinite bus. The regulator is responsive to changes in terminal voltage or in voltage at any specified point on the tie line and acts on the field voltage of the synchronous machine. (The "regulator" of this section includes the whole regulating and excitation system.) The notation and assumptions of Park[2] are followed.

For the synchronous machine

$$\begin{aligned} e_d &= pG(p)E - z_d(p)i_d + [x_q(p)i_q](p\theta) \\ e_q &= G(p)E(p\theta) - x_d(p)i_d(p\theta) - z_q(p)i_q \end{aligned} \qquad (1)$$

Similarly, for the tie line

$$\begin{aligned} e_d &= e \sin \delta + z(p)i_d - xi_q(p\theta) \\ e_q &= e \cos \delta + xi_d(p\theta) + z(p)i_q \end{aligned} \qquad (2)$$

Machine-torque equation

$$T = \psi_d i_q - \psi_q i_d + Mp^2\theta \qquad (3)$$

where

$$M = 4\pi fH$$

From equations 1, 2, and 3 the equations for small changes from a steady-state operating position are

$$\begin{aligned} \Delta e_d &= pG(p)\,\Delta E - z_d(p)\,\Delta i_d + x_q(p)\,\Delta i_q - \psi_{qo}p\,\Delta\delta \\ \Delta e_q &= G(p)\,\Delta E - x_d(p)\,\Delta i_d - z_q(p)\,\Delta i_q + \psi_{do}p\,\Delta\delta \\ \Delta e_d &= z(p)\,\Delta i_d - x\,\Delta i_q + (e \cos \delta - xi_{qo}p)\,\Delta\delta \\ \Delta e_q &= x\,\Delta i_d + z(p)\,\Delta i_q - (e \sin \delta - xi_{do}p)\,\Delta\delta \\ \Delta T &= i_{qo}G(p)\,\Delta E - [\psi_{qo} + i_{qo}x_d(p)]\,\Delta i_d + \\ &\quad [\psi_{do} + i_{do}x_q(p)]\,\Delta i_q + p^2M\,\Delta\delta \end{aligned} \qquad (4)$$

The regulator introduces a change in field voltage E as a function of the change in *magnitude* of the terminal voltage e_a

$$\Delta E = g(p)\,\Delta e_a \qquad (5)$$

where

$$e_a^2 = e_d^2 + e_q^2$$

and $g(p)$ is the operational expression for the action of the regulator

$$\begin{aligned} \Delta e_a &= e_d'\,\Delta e_d + e_q'\,\Delta e_q \\ e_d' &= e_{do}/e_{ao} \qquad e_q' = e_{qo}/e_{ao} \end{aligned} \qquad (6)$$

Then

$$\Delta E = g(p)(e_d'\,\Delta e_d + e_q'\,\Delta e_q) \qquad (7)$$

If equation 7 is substituted in equations 4, equations 4 become

$$\begin{array}{ccccccc} \Delta e_d & \Delta e_q & \Delta i_d & \Delta i_q & \Delta\delta & & \\ \hline i_{qo}G(p)g(p)e_d' & i_{qo}G(p)g(p)e_q' & -\psi_{qo} - i_{qo}x_d(p) & \psi_{do} + i_{do}x_q(p) & p^2M & = \Delta T & \\ pG(p)g(p)e_d' - 1 & pG(p)g(p)e_q' & -z_d(p) & x_q(p) & -\psi_{qo}p & = 0 & \\ G(p)g(p)e_d' & G(p)g(p)e_q' - 1 & -x_d(p) & -z_q(p) & \psi_{do}p & = 0 & \\ -1 & 0 & z(p) & -x & e \cos \delta - xi_{qo}p & = 0 & \\ 0 & -1 & x & z(p) & -e \sin \delta + xi_{do}p & = 0 & (8) \end{array}$$

where

$z(p) = r + px$ for the tie line

δ is the angle by which the machine leads the infinite bus voltage e.

In order to test the stability of the system, the determinant of the coefficients of equations 8 is expressed as a polynomial in p, and the signs of the real parts of the p roots are investigated by Routh's or Hurwitz's criterion.

The required initial steady-state conditions for given E, e, and δ are

$$i_{do}=\frac{-(r_d+r)e\sin\delta-(x+x_q)(e\cos\delta-E)}{(r_d+r)^2+(x+x_d)(x+x_q)}$$

$$i_{qo}=\frac{+(x+x_d)e\sin\delta-(r_d+r)(e\cos\delta-E)}{(r_d+r)^2+(x+x_d)(x+x_q)}$$

$$\psi_{do}=E-x_di_{do}\qquad e_{qo}=\psi_{do}-r_di_{qo}$$
$$\psi_{qo}=-x_qi_{qo}\qquad e_{do}=-\psi_{qo}-r_di_{do}\tag{9}$$

When amortisseur windings are neglected, the operational impedances are

$$x_d(p)=\frac{x_d'T_op+x_d}{T_op+1}\qquad x_q(p)=x_q$$

$$G(p)=\frac{1}{T_op+1}\tag{10}$$

If it is assumed that the regulator and exciter performance can be expressed as a static change (a) in field voltage E per unit change in terminal voltage e_a, together with a time lag expressible as an equivalent single time constant T_r, then

$$g(p)=\frac{a}{T_rp+1}\tag{11}$$

Other more exact operational relations may be used in the equations, but equations 10 and 11 are the simplest and are sufficiently exact for an initial study.

If components of voltage caused by the rates of change of flux linkage and of angle are neglected, equations 8 become

$$\begin{array}{ccccc|l}
\Delta e_d & \Delta e_q & \Delta i_d & \Delta i_q & \Delta\delta & \\
\hline
i_{qo}G(p)g(p)e_d' & i_{qo}G(p)g(p)e_q' & -\psi_{qo}-i_{qo}x_d(p) & \psi_{do}+i_{do}x_q(p) & p^2M & =\Delta T\\
-1 & 0 & -r_d & x_q(p) & 0 & =0\\
G(p)g(p)e_d' & G(p)g(p)e_q'-1 & -x_d(p) & -r_d & 0 & =0\\
-1 & 0 & r & -x & e\cos\delta & =0\\
0 & -1 & x & r & -e\sin\delta & =0
\end{array}\tag{12}$$

If, in addition, armature and line resistance are neglected, equations 8 are reduced to

$$\begin{array}{ccccc|l}
\Delta e_d & \Delta e_q & \Delta i_d & \Delta i_q & \Delta\delta & \\
\hline
i_{qo}G(p)g(p)e_d' & i_{qo}G(p)g(p)e_q' & -\psi_{qo}-i_{qo}x_d(p) & \psi_{do}+i_{do}x_q(p) & p^2M & =\Delta T\\
-1 & 0 & 0 & x_q(p) & 0 & =0\\
G(p)g(p)e_d' & G(p)g(p)e_q'-1 & -x_d(p) & 0 & 0 & =0\\
-1 & 0 & 0 & -x & e\cos\delta & =0\\
0 & -1 & x & 0 & -e\sin\delta & =0
\end{array}\tag{13}$$

Equations 13 imply that the voltage equations are reduced to the form

$$\Delta e_d=-\Delta\psi_q\qquad \Delta e_q=\Delta\psi_d\tag{14}$$

From equations 13 the characteristic determinant is

$$\begin{aligned}D={}&i_{qo}G(p)g(p)e_d'x_q(p)xe\cos\delta-\\
&[\psi_{qo}+i_{qo}x_d(p)][x_q(p)+x]e\sin\delta+\\
&[\psi_{do}+i_{do}x_q(p)][x_d(p)+x-xG(p)g(p)e_q']e\times\\
&\cos\delta+p^2M[x_q(p)+x][x_d(p)+x-\\
&xG(p)g(p)e_q']-\psi_{qo}[-\{x_q(p)+x\}\times\\
&G(p)g(p)e_q'e\sin\delta+\\
&x_q(p)G(p)g(p)e_d'e\cos\delta]\end{aligned}\tag{15}$$

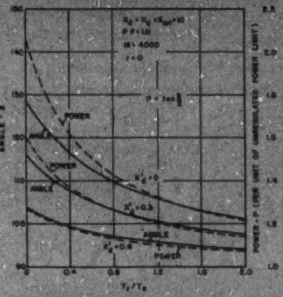

Figure 3. Effect of ratio of excitation and regulation-system time constant to field time constant on steady-state stability limit

Maximum stable operating conditions, by equation 34

For

$$x_d=x_q(p)=1.0,\ (\psi_{do}+i_{do}x_q(p))=E$$

B. Two Machines. The case of two synchronous machines with a voltage regulator responsive to the common bus-voltage magnitude and acting on either or both machine fields is considered in this section.

To simplify the analysis the armature resistances and the components of voltage caused by rate of change of flux and rate of change of machine angle were neglected finally in section A. These assumptions have been justified, not only from a common-sense consideration of the orders of magnitude of the quantities involved but also by the fact that test results have shown fairly good agreement with calculations. One then may write for each machine, as in equations 14

$$\Delta e_d=-\omega_0\Delta\psi_q$$
$$\Delta e_q=\omega_0\Delta\psi_d\tag{16}$$

For machine 1

$$\Delta e_{d1}=\omega_0x_{q1}(p)\Delta i_{q1}$$
$$\Delta e_{q1}=-\omega_0x_{d1}(p)\Delta i_{d1}+\omega_0G_1(p)\Delta E_1\tag{17}$$

For machine 2

$$\Delta e_{d2}=\omega_0x_{q2}(p)\Delta i_{q2}$$
$$\Delta e_{q2}=-\omega_0x_{d2}(p)\Delta i_{d2}+\omega_0G_2(p)\Delta E_2\tag{18}$$

Connection of the two machines to a common bus results in the relations

$$\begin{aligned}e_{d2}&=e_{d1}\cos\delta_{12}-e_{q1}\sin\delta_{12}\\
e_{q2}&=e_{d1}\sin\delta_{12}+e_{q1}\cos\delta_{12}\\
i_{d2}&=-i_{d1}\cos\delta_{12}+i_{q1}\sin\delta_{12}\\
i_{q2}&=-i_{d1}\sin\delta_{12}-i_{q1}\cos\delta_{12}\end{aligned}\tag{19}$$

or, by differentiating

$$\begin{aligned}\Delta e_{d2}={}&\Delta e_{d1}\cos\delta_{12}-\Delta e_{q1}\sin\delta_{12}-\\
&(e_{d1}\sin\delta_{12}+e_{q1}\cos\delta_{12})\Delta\delta_{12}\\
\Delta e_{q2}={}&\Delta e_{d1}\sin\delta_{12}+\Delta e_{q1}\cos\delta_{12}+\\
&(e_{d1}\cos\delta_{12}-e_{q1}\sin\delta_{12})\Delta\delta_{12}\\
\Delta i_{d2}={}&-\Delta i_{d1}\cos\delta_{12}+\Delta i_{q1}\sin\delta_{12}+\\
&(i_{d1}\sin\delta_{12}+i_{q1}\cos\delta_{12})\Delta\delta_{12}\\
\Delta i_{q2}={}&-\Delta i_{d1}\sin\delta_{12}-\Delta i_{q1}\cos\delta_{12}-\\
&(i_{d1}\cos\delta_{12}-i_{q1}\sin\delta_{12})\Delta\delta_{12}\end{aligned}\tag{20}$$

Substituting equations 20 in equations 18, one obtains

$$\begin{aligned}&\Delta e_{d1}\cos\delta_{12}-\Delta e_{q1}\sin\delta_{12}\\
&\quad=-\omega_0x_{q2}(p)(\Delta i_{d1}\sin\delta_{12}+\Delta i_{q1}\cos\delta_{12})+\\
&\qquad[e_{d1}\sin\delta_{12}+e_{q1}\cos\delta_{12}-\\
&\qquad\omega_0x_{q2}(p)(i_{d1}\cos\delta_{12}-i_{q1}\sin\delta_{12})]\Delta\delta_{12}\\
&\Delta e_{d1}\sin\delta_{12}+\Delta e_{q1}\cos\delta_{12}\\
&\quad=+\omega_0x_{d2}(p)(\Delta i_{d1}\cos\delta_{12}-\Delta i_{q1}\sin\delta_{12})-\\
&\qquad[e_{d1}\cos\delta_{12}-e_{q1}\sin\delta_{12}+\\
&\qquad\omega_0x_{d2}(p)(i_{d1}\sin\delta_{12}+i_{q1}\cos\delta_{12})]\Delta\delta_{12}+\\
&\qquad\omega_0G_2(p)\Delta E_2\end{aligned}\tag{21}$$

As in section A, the field voltages are given by

$$\Delta E_1=g_1(p)\Delta e_a=g_1(p)(e_{d1}'\Delta e_{d1}+e_{q1}'\Delta e_{q1})$$
$$\Delta E_2=g_2(p)\Delta e_a=g_2(p)(e_{d1}'\Delta e_{d1}+e_{q1}'\Delta e_{q1})\tag{22}$$

where $g_1(p)$, $g_2(p)$ are the regulator and excitation-system characteristics for machines 1 and 2, respectively, and the torque equation of machine 1 is

$$\begin{aligned}&i_{q1}G_1(p)g_1(p)(e_{d1}'\Delta e_{d1}+e_{q1}'\Delta e_{q1})-\\
&\quad[\psi_{q1}+i_{q1}x_{d1}(p)]\Delta i_{d1}+\\
&\quad[\psi_{d1}+i_{d1}x_{q1}(p)]\Delta i_{q1}+M_1p^2\Delta\delta_1=0\end{aligned}\tag{23}$$

Now from the voltage equations 19 and with the equations $e_d=-\omega_0\psi_q$ and $e_q=\omega_0\psi_d$ (by neglecting $p\psi$ and r_a), one may obtain

$$\psi_{q2}=\psi_{q1}\cos\delta_{12}+\psi_{d1}\sin\delta_{12}$$
$$\psi_{d2}=-\psi_{q1}\sin\delta_{12}+\psi_{d1}\cos\delta_{12}\tag{24}$$

Thus, the flux linkage is transformed just like the voltage, and it then may be shown that

$$i_{d1}\psi_{q1}-i_{q1}\psi_{d1}=-(i_{d2}\psi_{q2}-i_{q2}\psi_{d2})\tag{25}$$

As the electrical torques of the two machines are equal and opposite, and if there are no mechanically applied damping

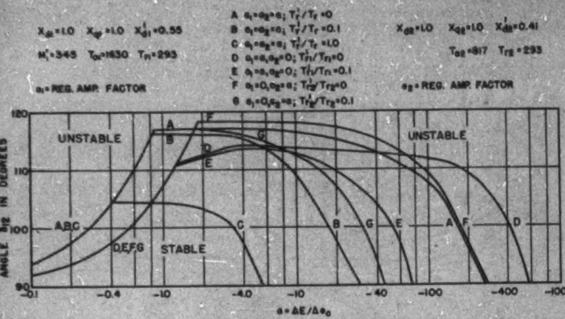

Figure 4. Effect of voltage regulators on steady-state stability limit

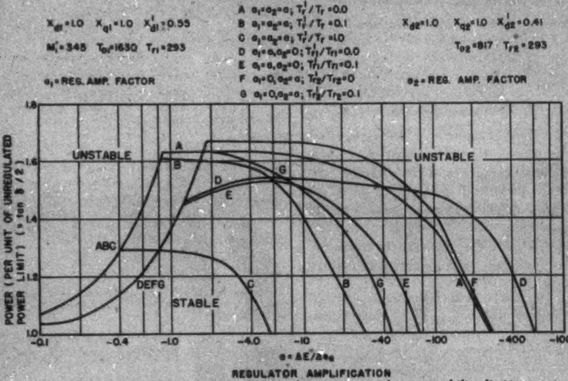

Figure 5. Effect of voltage regulators on steady-state stability limit

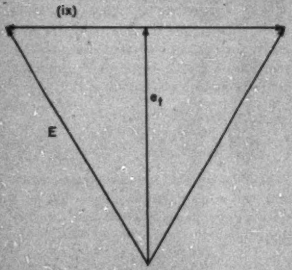

Figure 6

torques, the acceleration torques may be equated

$$M_1p^2\Delta\theta_1=-M_2p^2\Delta\theta_2$$

$$=\frac{M_1M_2}{M_1+M_2}p^2(\Delta\theta_1-\Delta\theta_2)=M'p^2\Delta\delta_{12}\quad(26)$$

where

$$M'=\frac{M_1M_2}{M_1+M_2}=\text{equivalent system inertia}$$

Thus $M'p^2\Delta\delta_{12}$ may be substituted for $M_1p^2\Delta\theta_1$ in equation 23 to eliminate the extra variable $\Delta\theta_1$ and the extra torque equation. This simplification is still valid even if there are mechanical rotor damping torques, *if* $(D_1/M_1)=(D_2/M_2)$, in which case $(M_1p^2+D_1p)\Delta\theta_1=(M'p^2+D'p)\Delta\delta_{12}$ where $D'=D_1D_2/D_1+D_2$. If $(D_1/M_1)\neq(D_2/M_2)$, two torque equations are required. Considering the case of no mechanical damping, required five system equations are 23, 17, and 21, or

$$\begin{array}{|c|c|c|c|c|l}
\Delta e_{d1} & \Delta e_{q1} & \Delta i_{d1} & \Delta i_{q1} & \Delta\delta_{12} & \\
\hline
i_{q1}G_1(p)g_1(p)e_{d1}' & i_{q1}G_1(p)g_1(p)e_{q1}' & -\psi_{q1}-i_{q1}x_{d1}(p) & \psi_{d1}+i_{d1}x_{q1}(p) & M'p^2 & =0\\
\hline
-1 & 0 & 0 & \omega_0x_{q1}(p) & 0 & =0\\
\hline
\omega_0G_1(p)g_1(p)e_{d1}' & \omega_0G_1(p)g_1(p)e_{q1}'-1 & -\omega_0x_{d1}(p) & 0 & 0 & =0\\
\hline
\cos\delta_{12} & -\sin\delta_{12} & \omega_0x_{q2}(p)\sin\delta_{12} & \omega_0x_{q2}(p)\cos\delta_{12} & -e_{q2}-\omega_0x_{q2}(p)i_{d2} & =0\\
\hline
\sin\delta-\omega_0G_2(p)g_2(p)e_{d1}' & \cos\delta-\omega_0G_2(p)g_2(p)e_{q1}' & -\omega_0x_{d2}(p)\cos\delta_{12} & \omega_0x_{d2}(p)\sin\delta_{12} & e_{d2}-\omega_0x_{d2}(p)i_{q2} & =0\\
\hline
\end{array}\quad(27)$$

The determinant of the coefficients of equations 11 is

$$\begin{aligned}&M'p^2\{x_{d1}x_{q1}+x_{d2}x_{q2}+(x_{d1}x_{q2}+x_{d2}x_{q1})\times\\
&\cos^2\delta+(x_{d1}x_{d2}+x_{q1}x_{q2})\sin^2\delta-G_1g_1\times\\
&[x_{q1}(x_{d2}-x_{q2})e_{d1}'\sin\delta\cos\delta+\\
&e_{q1}'(x_{d2}x_{q2}+x_{d2}x_{q1}\cos^2\delta+x_{q1}x_{q2}\sin^2\delta)]-\\
&G_2g_2[x_{q1}(x_{d1}+x_{q2})e_{d1}'\sin\delta+x_{d1}\times\\
&(x_{q1}+x_{q2})e_{q1}'\cos\delta]\}+(\psi_{d1}+i_{d1}x_{q1})\times\\
&\{(e_{d2}-x_{d2}i_{q2})[(G_1g_1e_{q1}'-1)x_{q2}-x_{d1}]\times\\
&\sin\delta-(e_{q2}+x_{q2}i_{d2})([(G_1g_1e_{q1}'-1)x_{d2}-x_{d1}]\times\\
&\cos\delta+x_{d1}G_2g_2e_{q1}')\}+(\psi_{q1}+i_{q1}x_{d1})\times\\
&(G_2g_2e_{d1}'x_{q1})(e_{q2}+x_{q2}i_{d2})+G_1g_1e_{d1}'x_{q1}\times\\
&\{(e_{d2}-x_{d2}i_{q2})(\psi_{q1}-i_{q1}x_{q2})\sin\delta-\\
&(e_{q2}+x_{q2}i_{d2})(\psi_{q1}-i_{q1}x_{d2})\cos\delta\}+\\
&[\psi_{q1}(G_1g_1e_{q1}'-1)-i_{q1}x_{d1}][(e_{d2}-x_{d2}i_{q2})\times\\
&(x_{q1}+x_{q2})\cos\delta+(e_{q2}+x_{q2}i_{d2})\times\\
&(x_{q1}+x_{d2})\sin\delta]\quad(28)\end{aligned}$$

where in equation 28 $G_1(p)$, $x_{d1}(p)$, and so forth have been written for convenience as G_1, x_{d1}, and so forth; ω_0 has been taken as 1.0; and δ_{12} has been written simply as δ.

For the special case $x_{d1}(o)=x_{d2}(o)=x_{q1}(p)=x_{q2}(p)=e_a=$ power factor $=1.0$ (two round-rotor machines with equal steady-state reactances and equal excitations) the determinant 28 reduces to

$$\begin{aligned}&2\cos^2\frac{\delta}{2}M'p^2\Big\{[x_{d1}(p)+x_{d2}(p)]\times\\
&(1+\cos^2\delta)+[1+x_{d1}(p)x_{d2}(p)]\sin^2\delta-\\
&[G_1(p)g_1(p)+G_2(p)g_2(p)]\sin\delta\sin\frac{\delta}{2}-\\
&2[G_1(p)g_1(p)x_{d2}(p)+G_2(p)g_2(p)x_{d1}\times\\
&(p)]\cos^2\frac{\delta}{2}\Big\}+[(2-\cos\delta)-(2+\cos\delta)\times\\
&x_{d1}(p)x_{d2}(p)]\sin^2\delta+[x_{d1}(p)+\\
&x_{d2}(p)](2+\sin^2\delta)\cos\delta-[G_1(p)g_1(p)]+\\
&G_2(p)g_2(p)]\sin\delta\sin\frac{\delta}{2}-2[G_1(p)g_1(p)x_{d2}(p)+\\
&G_2(p)g_2(p)x_{d1}(p)]\cos^2\frac{\delta}{2}\quad(29)\end{aligned}$$

Application

A. SINGLE MACHINE AND TIE LINE

Equation 15 (or the similar equations derivable from equations 12 or 8) can be used to determine the stability limits of the single machine system over a range of circuit constants. The quantities which can be varied are:

1. Regulator amplification factor a.
2. Machine field time constant T_o.
3. Regulator and exciter time constant T_r (for the simple case of equation 11).

4. External reactance x.

5. Terminal power factor.

6. Transient reactance x_d'.

7. Moment of inertia M.

8. Location of regulator (that is, at machine terminals or along tie line).

9. Quadrature axis reactance x_q.

10. External resistance r.

11. Amortisseur characteristics.

12. More complex regulating systems, including stabilizers.

A study of the effects of the first three of these quantities has been made with the remaining quantities fixed at the values

$x = x_q = x_d = 1.0$
Power factor $= 1.0$
$x_d' = 0.3$
$M = 4{,}000$ (or $H = 5.3$)
Simple regulator at machine terminals
$r = 0$ (30)
No amortisseur

Under these conditions the maximum operating angle with no regulator is $\delta = 90$ degrees, and with unit terminal voltage ($v_e = 1.0$) the machine field and infinite bus voltages are $E = e = (\cos\delta/2)^{-1}$. Also, from equations 9

$$i_{do} = \frac{\sin^2 \frac{\delta}{2}}{\cos \frac{\delta}{2}} \qquad \psi_{qo} = -i_{qo} = -\sin\frac{\delta}{2}$$

$$e_{do}' = e_{do} = -\psi_{qo} = \sin\frac{\delta}{2}$$

$$i_{qo} = \sin\frac{\delta}{2}$$

$$\psi_{do} = \cos\frac{\delta}{2} \qquad e_{qo}' = e_{qo} = \psi_{do} = \cos\frac{\delta}{2} \tag{31}$$

From equations 10

$$x_d(p) = \frac{0{\cdot}3T_e p + 1}{T_e p + 1}$$

$$x_q(p) = 1$$

$$G(p) = \frac{1}{T_e p + 1}$$

If some of these relations are substituted in equation 15, it becomes

$$+p^4 4M[+T_r T_e(1+x_d')]\cos^3\frac{\delta}{2}$$
$$+p^3 4M[T_e(1+x_d')+2T_r]\cos^3\frac{\delta}{2}$$
$$+p^2\left\{4M\left(2-a\cos\frac{\delta}{2}\right)\cos^3\frac{\delta}{2}+\right.$$
$$\left.2T_r T_e[(1+x_d')\cos\delta+(1-x_d')\sin^2\delta]\right\}$$
$$+p\{2T_e[(1+x_d')\cos\delta+(1-x_d')\sin^2\delta]+4T_r\cos\delta\}$$
$$+\left(4\cos\delta-2a\cos\frac{\delta}{2}\right) \tag{32}$$

Equation 32 is in a form such that, in addition to the quantities studied, the rotor-inertia M and transient-reactance x_d' can be varied also.

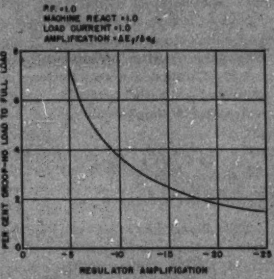

Figure 7. Relation between regulator amplification and voltage droop

Results for Single Machine. Figures 1 and 2 show some of the results obtained. Figure 1 shows the maximum permissible operating angles between the machine and the bus, and Figure 2 shows the corresponding power limits as functions of the amplification factor a of the regulating system. Curve E, for no time lags in either the regulator or machine field, that is, for $T_r = T_e = 0$, is seen from equation 32 to be given by the simple formula

$$a = \frac{2\cos\delta}{\cos\frac{\delta}{2}} \tag{33}$$

The maximum stable operating angle is $\delta = 180$ degrees (90 degrees between the machine direct axis and its terminals and 90 degrees between the machine terminals and the bus voltage) for $a = -\infty$, with a corresponding infinite power (see section, "Preliminary Remarks").

If $T_e \neq 0$ (with $T_r = 0$ still) the possible increase in power limit is at once very much reduced, and an upper limit of $(-a)$ is found (see curve A). It may be shown that, for all of the curves of Figures 1 and 2, the maximum stable operating angle occurs when the coefficients of p^1 and p^0 in equation 3 are simultaneously equal to zero. For these cases the maximum possible stable operating angle is given by the formula

$$\cos\delta = b - \sqrt{b^2+1} \tag{34}$$

where

$$b = \frac{\left(1 + x_d' + 2\frac{T_r}{T_e}\right)}{2(1-x_d')}$$

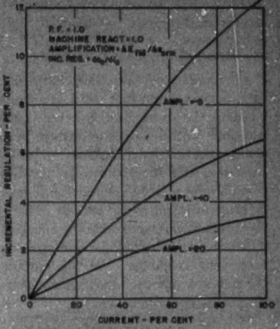

Figure 8. Relation between incremental regulation and regulator amplification

Figure 3 shows a plot of equation 34. This equation has been verified only in the range studied and should not be used over a much wider range without further check calculations by equation 32. The required values of regulator amplification factor a corresponding to the conditions of Figure 3 may be found from equation 33.

If $T_r/T_e = 0$, the maximum possible stable operating angle is that corresponding to $T_e = \infty$. This may be seen by comparing equation 34 to the case $T_e = \infty$, $T_r = 0$ in equation 32. Setting $T_e = \infty$ is equivalent to maintaining constant field flux linkages, in which case the steady-state power limit may be calculated by replacing the direct-axis synchronous reactance by the direct-axis transient reactance in the salient-pole synchronous-machine power-transfer formula, and the regulator action need not be considered. (Similarly, in the case of two regulated machines, the direct-axis reactances of both machines may be replaced in the limit by the direct-axis transient reactances.) Note that the power limits so obtained may be considerably different from those which would be found by replacing both x_d and x_q by x_d'.

Curves B, C, and D of Figures 1 and 2 show the further reduction caused by the introduction of a time lag in the regulation and excitation systems. Although a time lag decreases the maximum attainable angle and power limit, it allows operation over a considerably greater range in regulator amplification factor a.

If $T_e = 0$ but $T_r > 0$, no improvement in stability is possible. To the contrary, any attempt to control the field voltage reduces the stability limit. This is evident from equation 32. The detrimental effect of a high ratio of T_r/T_e is also evident from Figure 3.

B. Results for Two Machines

1. *Location of Regulated Excitation.* It is not necessary that the field of each machine be controlled by the same amount. If there is any advantage in so doing, the relative amounts of field-excitation change produced on the two machines for a given voltage change may be made anything within reason. Therefore, calculations were made (by equation 29) to determine what the optimum arrangement should be. For this purpose the regulator and excitation-system characteristics were represented by

$$g(p)=\frac{a}{T_rp+1}=\frac{a}{293p+1} \tag{35}$$

This is the simplest possible characteristic; the exciter is assumed to have a 293-radian time constant, and all other lags in the regulating system are neglected.

Three arrangements were studied:

a. Regulation only on machine 1

$$g_1(p)=\frac{a}{293p+1} \quad g_2(p)=0 \tag{36a}$$

b. Regulation equal on both machines

$$g_1(p)=g_2(p)=\frac{a}{293p+1} \tag{36b}$$

c. Regulation only on machine 2

$$g_1(p)=0 \quad g_2(p)=\frac{a}{293p+1} \tag{36c}$$

Figures 4 and 5, curves A, D, and F, show the results and indicate that there is little difference among the three arrangements, so that the amount of stability gain is not critical to the relative field-excitation characteristics. The values of optimum amplification factor are smaller if the voltage regulation is equal on both machines, but this is only what should be expected, since the total required changes of field current on both machines tend to remain constant.

Comparison of Figures 4 and 5 with 1 and 2 shows also that the possible gain with two more or less similar machines is greater than for one machine and tie line. This appears to be a reasonable result when it is recalled that the lower the transient reactance the more the gain, and that the total transient reactance of two machines is, of course, smaller than that of one machine and a tie line.

2. *Additional Lags in Regulating System.* Since an actual regulating system may be made up of several stages, each having some time lag, the next step is to find out how important these lags are. The three regulator arrangements discussed in section 1 were, therefore, recalculated with the regulating system represented by the expression

$$g(p)=\frac{a}{(T_rp+1)(T_r'p+1)} = \frac{a}{(293p+1)(29.3p+1)} \tag{37}$$

This allows for a lag of $T_r'=29.3$ radians in the regulator in addition to the lag of $T_r=293$ radians in the exciter. The amount of possible gain in stability is seen by curves B, E, and G of Figures 4 and 5 to be somewhat smaller. Also, the permissible range of regulation or of amplification factor a is restricted still further. Although the reduction in gain is not alarming, it is sufficient to indicate that every effort should be made to reduce regulator lag.

Figures 4 and 5 also show curves C for the case of equal regulating effort on both machines with a 293-radian time lag T_r' in the regulator

$$g(p)=\frac{a}{(293p+1)^2} \tag{38}$$

The possible gain and allowable range of regulation are reduced greatly by this larger additional lag, as in the case of one machine.

Appendix. Relation Between Regulator and Excitation-System Amplification and Voltage Regulation

Figures 7 and 8 have been computed to show what the amplification factor (a) discussed in this paper means in terms of voltage regulation of the system. The vector diagram is as shown in Figure 6 for two equal round-rotor machines. From the diagram

$$e_t^2+(ix)^2=E^2 \tag{39}$$

For the regulated excitation system

$$E=E_o-a(e_{to}-e_t) \tag{40}$$

whence the equation for e_t is

$$(a^2-1)e_t^2+2a(E_o-ae_{to})e_t+(E_o-ae_{to})^2-(ix)^2=0 \tag{41}$$

with

$$E_o=e_{to}=ix=1.0$$

$$(a^2-1)e_t^2+2a(1-a)e_t+(1-a)^2-1=0 \tag{42}$$

Equation 42 allows one to calculate the voltage droop from no load ($i=0$) to full load ($ix=1$) for any amplification factor a. Results are shown in Figure 7.

Also, from equations 39 and 40 one obtains for the incremental regulation, defined as de_t/di, the relation

$$\frac{de_t}{di}=-\frac{x^2i_o}{(e_{to}-E_oa)} \tag{43}$$

Results are shown in Figure 8.

The conclusions for this unity terminal power-factor case are that the over-all droop is about 40 per cent of the negative of the reciprocal of the amplification factor, and the incremental regulation increases from zero with increasing initial load, being equal to the over-all droop at about half load.

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A New Differential Analyzer

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Synopsis: This paper describes a new 14-integrator differential analyzer recently placed in service by the General Electric Company at Schenectady. This analyzer has a mechanical interconnection system but has integrators of novel design in that a Polaroid follow-up system is used rather than the more usual mechanical torque amplifiers.

TECHNICAL progress creates a need for tools which are capable of analyzing physical phenomena more accurately and rapidly. Electric calculating devices (d-c calculating boards, a-c network analyzers, and transient analyzers) represent significant attainments in fulfilling this growing need.

The development of the differential analyzer, both in this country and abroad, has kept a timely pace with technical progress. Early contributions to this development led to the construction of such an analyzer at the Massachusetts Institute of Technology.[3] Since that time several other similar analyzers have been constructed.[4,5,6,9,10,12,13] The value of such a device as a precision tool for the purpose of solving ordinary differential equations has been well established. Since 1935 General Electric Company engineers have used the University of Pennsylvania differential analyzer for the solution of many important problems. Most of the studies were made jointly with members of the Moore School staff, and some were of sufficient general interest to result in published articles.

With this experience and the increasingly important part played by the other various types of analyzers in carrying on analytical engineering work, the General Electric Company has built and recently put into operation at Schenectady a new differential analyzer. It is the purpose of this paper to describe this analyzer.

Fundamental Principles

The differential analyzer is fundamentally a device for the automatic solution of differential equations. One of the most important component parts of such a device is the means for accomplishing integration. While several known devices may serve as integrators, one which has served this purpose well over a number of years is the Kelvin wheel-and-disk type. This device is best described functionally by referring to Figure 1. In this figure the angular rotation of the turntable or disk, α, is proportional to the independent variable. The point of contact between the integrator wheel of radius R and this disk is restricted to a straight-line parallel to the axis of rotation of the wheel and passing through the center of the turntable. Its radial position from the center of this disk is defined as r. If there is no slip between the wheel and disk, then at any instant the arc $rd\alpha$ is equal to the arc $Rd\beta$, and, therefore

$$d\beta=\frac{r}{R}d\alpha$$

Consequently

$$\beta=\frac{1}{R}\int_{\alpha_1}^{\alpha_2} rd\alpha$$

and it follows that the angular rotation of the integrator wheel is the integral of the variable radius r with respect to the angular rotation α of the turntable between the limits, as defined by α_1 and α_2 with proper regard for signs and mechanical constants of the device. In such a device the radius r may be controlled by the rotation of a lead screw. Thus, integration can be accomplished in terms of the angular rotation of three shafts, namely, the turntable drive shaft, the lead-screw shaft, and the integrator-wheel shaft, the last always being the integral of the angular rotation of the lead-screw shaft with respect to the angular rotation of the turntable drive shaft.

Although these are the essential parts of an integrating device of this type, one additional element is most important in making it a practical device. In the solution of differential equations it is invariably necessary for the rotation of the integrator wheel to produce sufficient torque to drive the output shaft. The lightweight integrator wheel in contact with the turntable is not capable of producing this required torque. Some means of translating the angular position of the integrator wheel to another shaft through a means capable of supplying the required torque is necessary. The newly developed Polaroid follow-up system used to accomplish this purpose in the new General Electric differential analyzer will be described later in this paper.

The basic elements of a complete differential analyzer are shown in Figure 2. In this figure an input table, an output table, and an integrator are shown connected together with a motor which provides angular rotation proportional to the independent variable. As this motor rotates the integrator turntable rotates, and, simultaneously, the input- and output-table horizontal lead screws rotate, causing the carriage supporting the vertical lead-screw assemblies to move toward the right on both input and output tables. If, as this motion takes place, the crank on the splined shaft of the input table is rotated so as to keep a pointer on the curve drawn, and if this cranked angular rotation is made to drive the lead screw of the integrator, then the angular-rotation β of the integrator wheel is the integral of the function being followed on the input table with respect to the angular-rotation α of the motor drive, which is the independent variable. The angular-rotation β of the integrator wheel is transmitted through a follow-up system to the vertical lead-screw drive of the output table. Thus, the pencil supported by the output-

Table I. Integrator-Wheel Slip Measurements

Integrator Lead-Screw Setting	Integrator-Wheel Rpm	Equivalent Revolutions		Per Cent Difference	Output Torque (Pound-Inches)
		Integrator Wheel	Turntable		
−64.00	1,000	1,006.00	1,006.30	+0.030	0
−64.00	480	1,000.52	1,000.99	+0.047	0
−64.00	240	993.15	993.50	+0.035	0
−48.00	480	1,003.20	1,003.35	+0.015	0
−32.00	480	1,009.72	1,009.89	+0.017	0
−16.00	480	1,004.81	1,005.07	+0.026	0
+64.00	480	994.81	994.93	+0.008	0
+48.00	480	992.53	992.45	−0.008	0
+32.00	480	1,000.21	1,000.33	+0.012	0
+16.00	480	1,001.04	1,000.35	−0.069	0
+64.00	1,000	968.56	968.49	−0.007	5.0
+64.00	1,000	996.26	996.19	−0.007	6.0
+64.00	1,000	996.04	995.99	−0.005	9.0

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table carriage traces a curve which is the integral of the curve $r = K\alpha$ on the input table, with respect to the independent variable α, appropriate regard being given to constants and boundary conditions.

It is not always necessary to crank in functions, for, in many cases, they can be generated. In the example shown, for instance, the function $r = K\alpha$ being integrated is proportional to the independent variable α and, consequently, could be obtained directly from the motor drive with suitable gear ratios. However, this example was used as an elementary illustration of the function of the fundamental component parts of a differential analyzer. In general, input tables are used to crank in experimental functions which cannot be expressed conveniently analytically, but which, nevertheless, are known.

Design Considerations

Experience with the differential analyzer of the Moore School at the University of Pennsylvania in Philadelphia served as a basis for establishing certain criteria of performance which were desired in such a device to best meet the particular needs. Among the desired objectives were:

1. Flexibility.
2. Speed of solution.
3. Accuracy.
4. Low maintenance.
5. Reasonable time and cost required for construction.

While successful and satisfactory means for solving differential equations need not be patterned after existing analyzers, and it is realized that other means may offer excellent possibilities, these possibilities could be realized only after an extended development program. On the other hand, the use of an analyzer as a tool for the solution of design and system problems was desired at the earliest possible date. This fact played an important part in the formulation of the plans for building such a device.

Experience with the mechanical interconnection system at the University of Pennsylvania, which is similar to that employed in the analyzer described by Vannevar Bush, indicated that this system has a high degree of flexibility that is very desirable in a device to be used for the solution of many different types of problems. Such a system has proved satisfactory over a number of years of relatively continuous operation in several analyzers. With the wholehearted cooperation of the University of Pennsylvania, drawings, patterns, and so forth, already completed for this part of the analyzer were made available. Professor Irven Travis of the University of Pennsylvania, now in active service, was made available as consultant.

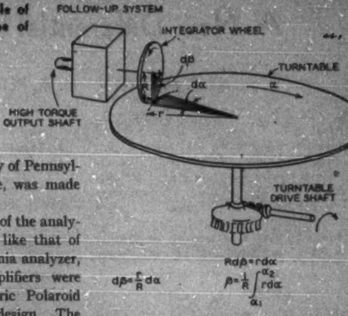

Figure 1. Fundamental principle of the Kelvin wheel-and-disk type of mechanical integrator

While the mechanical part of the analyzer was constructed mainly like that of the University of Pennsylvania analyzer, the mechanical torque amplifiers were replaced by General Electric Polaroid follow-up systems of novel design. The use of these new devices made it possible to make desirable and significant changes in the design of the integrator itself. Tests on these new integrators have shown that they contribute toward improved analyzer performance. This improved performance is realized in attaining a shorter solution time without sacrificing the desired degree of accuracy. Also, since adjustments once made need not be made periodically, as is the case with the conventional mechanical analyzer, time lost through maintenance is kept at a minimum.

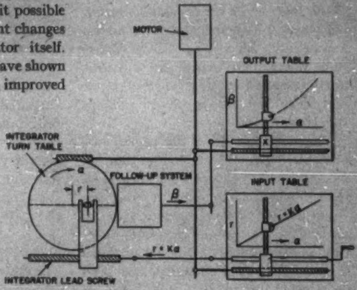

Figure 2. Schematic representation of the interconnections of the basic differential-analyzer elements

$$\beta = \frac{1}{R}\int_0^{a_1} K\alpha\,d\alpha = \frac{K}{2R}a^2$$

The Polaroid Follow-up System

Mechanical torque amplifiers, as used on existing analyzers, require that, in order to change the position of the output shaft and produce driving torque, a finite torque be applied to the integrator wheel. Thus, there is a finite ratio between output and input torques, and the device is correctly named a torque amplifier. In contrast with this, the new Polaroid follow-up system is not a torque amplifier. The position of the output shaft is controlled by the position of the integrator wheel by means of polarized light beams which form the only link between the integrator wheel and the output shaft. Consequently, there can be no direct feedback, and the concept of a relation between input and output torques has no significance.

The operation of this Polaroid follow-up

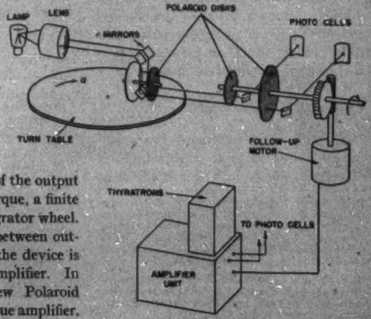

Figure 3. Schematic representation of the Polaroid follow-up system

system[11-14] can be explained best by referring to Figure 3. On the shaft of the integrator wheel, which is in contact with the turntable, is rigidly mounted a Polaroid disk. This shaft turns on jeweled bearings as the turntable is rotated. Light from a lamp source is brought through a condensing lens and infrared eliminating filter system. The infrared filter is necessary in order to eliminate that portion of the spectrum which is least polarized by the Polaroid system. Light from the condensing lens is directed to a set of mirrors supported rigidly on the integrator-wheel carriage. These mirrors reflect the incoming horizontal light vertically downward as two separate parallel beams. These beams in turn are intercepted by a second set of mirrors directly below, which reflect the incoming light as two horizontal beams which pass through the Polaroid disk. This Polaroid disk polarizes the two beams of light in the direction of the axis of polarization indicated by the parallel lines shown. These two parallel beams of polarized light continue and pass through separate Polaroid disks on the output shaft. One of these Polaroid disks is made larger than the other, and the axes of the integrator wheel and output shaft are displaced in parallel relation in order to make this separation of light beams possible. The two output-shaft Polaroid disks have their axes of polarization displaced by 90 degrees with respect to each other. Each beam, as it comes through its respective Polaroid disk, is again reflected horizontally at right angles to the incident beam. Each of these two beams falls upon a photocell.

If the axis of polarization of the Polaroid disk on the integrator-wheel shaft lies intermediate between the two axes of polarization of the Polaroids on the output shaft, then the light intensity is the same on both photocells. If the integrator wheel becomes slightly displaced from this intermediate position of balanced light, then one photocell receives more and the other less light. The direction of this displacement determines which of the two photocells receives more light, and, consequently, which receives less.

The two photocells form two branches of a bridge circuit which is balanced when the axis of the Polaroid disk on the integrator wheel shaft is intermediate between the axes of polarization of the two Polaroid disks on the output shaft. Any unbalance between the two light beams falling on the photocells causes a corresponding directional unbalance in this bridge circuit, which in turn is amplified, so as to control the firing of thyratron tubes. The controlled current through these tubes flows through a split-field series follow-up motor, causing it to turn in the direction required to maintain the balance of the photocell bridge circuit.

The basic circuit required to translate the photocell signal to the motor driving the output shaft is shown schematically in Figure 4. It is seen that the photocells are connected in series, and the midpoint is connected to the grid of one of the amplifier tubes. The grid of the second amplifier tube is adjusted and maintained at a potential so as to equalize the plate voltages on the two amplifier tubes when the two photocells receive an equal amount of light corresponding to balance. Thus, under balanced light conditions, the voltage from plate to plate of the amplifier tubes is zero. A displacement of the integrator wheel in one direction, with the output shaft stationary, causes a d-c voltage to appear between these two plates. If the direction of displacement is reversed, the polarity of this d-c voltage is reversed. The desired directional signal is thus available between the plates of the two amplifier tubes. This signal in turn is brought to the grids of the two thyratron tubes, making one grid positive with respect to the other for a displacement of the integrator wheel in one direction, and one grid negative with respect to the other one for a displacement in the opposite direction. Thus, with one thyratron in each half of the split-field winding, and with alternating current applied to the thyratrons, field and armature in series as shown in Figure 4, a displacement of the integrator wheel with respect to the output shaft causes one or the other thyratron to conduct, the one conducting depending on the direction of this displacement.

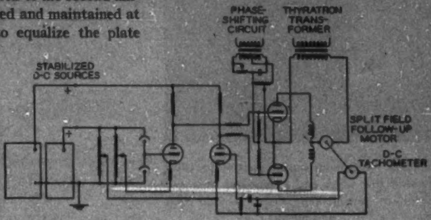

Figure 4. Schematic circuit diagram of the photocell amplifier and thyratron follow-up motor drive

It is desirable under balanced conditions that these two thyratrons conduct a small current equal in the two tubes for high sensitivity, thus producing no average motor torque. Also, the motor torque should increase rapidly and smoothly with angular displacement of the integrator wheel from the position of balance. In order to accomplish this, an a-c voltage adjustable in amplitude and phase with respect to the alternating current applied to the motor is introduced, as shown in Figure 4. When this a-c grid voltage

Table II. Typical Sine-Wave Growth Measurements

Maximum Integrator-Wheel Speed	Amplitude (Revolutions of Lead Screw)	Average Growth Per Cycle (Revolutions)	Per Cent Growth Per Cycle	Step Down	Gears for Train
970	50.00	0.525	1.05	None	None
780	40.00	0.400	1.00	None	None
390	20.00	0.194	0.97	None	None
510	60.00	0.225	0.375	None	None
335	40.00	0.175	0.437	None	None
170	20.00	0.135	0.675	None	None
262	60.00	0.135	0.225	None	None
175	40.00	0.128	0.320	None	None
87	20.00	0.104	0.520	None	None
200	64.00	0.129	0.202	1:4	1 pair
200	32.00	0.130	0.406	1:4	1 pair
200	16.00	0.123	0.770	1:4	1 pair
600	64.00	0.158	0.247	1:4	1 pair
1,000	64.00	0.174	0.272	1:4	1 pair
600	32.00	0.147	0.459	1:4	1 pair
1,000	32.00	0.162	0.506	1:4	1 pair
200	64.00	0.090	0.141	1:16	2 pairs
200	32.00	0.091	0.284	1:16	2 pairs
200	16.00	0.084	0.525	1:16	2 pairs
600	64.00	0.103	0.161	1:16	2 pairs
1,000	64.00	0.100	0.156	1:16	2 pairs
600	32.00	0.091	0.284	1:16	2 pairs
1,000	32.00	0.096	0.300	1:16	2 pairs

Figure 5. Integrator output-shaft motor drive showing the worm and compound gear and the optical arrangement of the Polaroid disks and mirrors

In back of the motor drive is shown the magnetic-clutch arrangement in the integrator lead-screw shaft

Figure 7. Top view of one of the double integrator-unit assemblies

The steel cabinet underneath the integrator table contains the thyratron transformer, auxiliary transformers, time-delay relay for the thyratron plate circuits, and various switches

Figure 6. Top view of integrator unit showing the details of the carriage assembly and of the carriage drive

lags the a-c motor voltage by approximately 90 degrees, the d-c voltage from the amplifier causes firing of the thyratrons over increasing portions of the positive half of the a-c motor voltage wave, the portion being continuously increased over the entire positive half cycle depending upon the magnitude of the d-c signal from the amplifier. Thus, the sensitivity is determined mainly by the d-c volts output per degree of angular displacement of the integrator wheel with respect to the balance point corresponding to equal illumination on the two photocells.

It is well known that, in sensitive follow-up systems such as this, stability is one of the most difficult problems. In particular, where the conditions of operation, as to speed, load, acceleration, inertia, and reversing, range over the extremes which are necessarily encountered in operating a flexible device such as this one, it attains most difficult proportions. Some effective means of stabilization is essential to satisfactory performance. One means of attaining stability is illustrated in Figure 4. The d-c tachometer shown is mounted on the same shaft as the d-c motor armature. It furnishes an output voltage proportional to speed which is used as an indication of output-shaft speed, and also through a resistance-capacitance network gives a signal across a resistance which has a component proportional to rate of change of speed. This signal is applied to the grid of the second amplifier tube as shown. In the case of a quick displacement of the integrator wheel requiring high acceleration of the motor, this signal partially counteracts the signal due to the photocell light unbalance, thus causing the output shaft to approach the position of balance more gradually than would be possible otherwise. Hence, with proper adjustment, the tendency to overshoot is diminished, and sustained oscillations or "hunting" cannot exist. This system of stabilization has been used with good results.

When several integrators are cascaded, the general stability problem becomes more difficult. Substantial progress toward the goal of further improved stability under these conditions has been made.

Figure 5 illustrates the output-shaft motor drive with cover removed. This shows the two output-shaft Polaroid disks and mirrors which reflect the two polarized light beams on the photocells. It also shows the follow-up motor mounting and the worm-gear drive. The worm gear is made of a special compound to withstand mechanical wear. Figure 6 is a top view of the integrator showing the carriage supporting the integrator wheel, Polaroid disk, mirror assembly, and the glass turntable.

The Integrators

The integrators fundamentally are of the Kelvin disk-and-wheel type, as illustrated in Figure 1. A top view of two associated integrator units is shown in Figure 7. In the design of the complete integrator every effort was made to take advantage of the light-beam system of transmission to obtain a freely running, stable, precision unit.

Since there is no mechanical connection between the integrator-wheel shaft and the output shaft, it is possible to move the integrator-wheel assembly in its carriage parallel to the lead screw rather than to move the entire turntable with respect to the integrator-wheel assembly. Thus, the axis of rotation of the turntable remains fixed in position, and the turntable is rotated by means of a simple right-angle

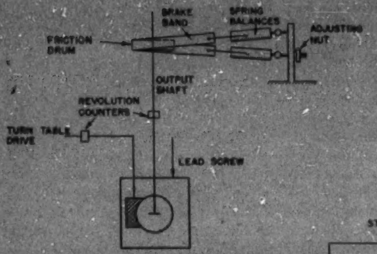

Figure 8. Mechanical integrator connections for slippage performance tests of Table I

The integrator output torque is adjusted by means of friction drum, brake band, and spring balances. Without slippage the revolution counters in the turntable drive and output shaft show a constant relationship

drive, no splined shafts being necessary. The lead screw for the integrator-wheel carriage is parallel to the directed light beams, so that, for any setting of the lead screw in the proper range of carriage travel, the light beams always will be directed properly through the optical system.

The integrator-wheel assembly is mounted in a pivoted support which permits free vertical movement of the wheel, thus assuring that it will remain in contact with the plate-glass disk. The wheel is of stainless steel, two inches in diameter, with a rim having a $^1/_{16}$-inch radius of curvature. Only a light pressure is required between the wheel and turntable, because, regardless of output-shaft load, all that is needed is sufficient friction force to turn the light integrator-wheel-shaft assembly in its jeweled bearings. The carriage is, thus, merely a guide for the almost frictionless balanced integrator wheel. Statically, there is very little pressure on the jeweled bearing surfaces; dynamically, there is pressure depending upon acceleration, principally, but this pressure is equalized between the two jeweled bearings because of the unique arrangement of counterweights.

Accurate alignment of the locus of the point of contact between integrator wheel and glass disk is maintained by means of ball-bearing contacts with guide rails. The vertical guide rail on the lead-screw side serves as the true guide, since the four ball bearings on this side (two making contact with the vertical guide and two with the horizontal one) are fastened rigidly to the carriage. The single bearing making contact with the horizontal guide on the side opposite the lead screw is also fastened rigidly to the carriage. Thus, the weight of the carriage is supported by three points of contact with the horizontal guide rails. The two ball bearings running on the vertical guide rail opposite the lead-screw side are spring supported on the carriage, thus causing the carriage to be held against the vertical guide on the lead-screw side.

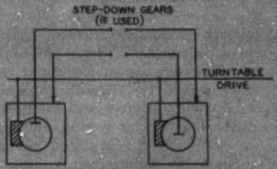

Figure 9. Mechanical interconnections of two integrator units for the generation of sine–cosine curves

This arrangement was used for the sine-wave growth tests of Table II

A lead-screw revolution indicator is shown in Figure 7, which is made up of two dials, one of which is graduated in hundredths of a revolution. The second is stepped down by means of gears to read complete revolutions over a range from +65 to −65. Thus, boundary conditions may be set to hundredths of a revolution. A split nut is pivoted in the carriage assembly and makes contact with the lead screw. Adjustable spring tension makes it possible to maintain the assembly free from back lash. A simple solenoid-operated clutch release is used which is positive in operation, and a push button is conveniently located so that boundary conditions can be set easily while the clutch is held in the released position.

Ventilating covers are provided for the thyratron tubes as shown in Figure 7. Screw adjustments for proper setting of the amplifier circuit constants are provided on the side of each unit.

Two limit switch controls on the integrator carriage are used in order to protect against possible overtravel and consequent damage. These are arranged in sequence, so that, as the normal range of travel is exceeded, the first switch stops the time motor. If travel of the carriage continues beyond this point, the second, which is an emergency switch, releases the clutch in the integrator lead screw.

Performance Data

The integrators were designed to operate at a maximum output-shaft speed of 1,000 rpm. One of the limitations on maximum permissible speed is that of slipping between the integrator wheel and the glass disk. In order to determine whether or not this was an important factor over the speed range up to 1,000 rpm, numerous tests have been made. The test set-up used for this purpose and for the purpose of determining the over-all mechanical accuracy is shown in Figure 8.

It will be recognized that, for each setting of the lead screw, there should be a fixed ratio between the number of revolutions of the output shaft and the turntable shaft. The revolution counters on these two shafts serve to add up the number of revolutions for each of these shafts and, when reduced to a common base of "equivalent revolutions" as required by the particular lead-screw setting, will serve as a means of evaluating the amount of slipping and over-all mechanical accuracy. For instance, since the lead screws have 16 threads per inch, and since the integrator wheel is two inches in diameter, there should be a 1:1 correspondence between the revolutions of the turntable shaft and the output shaft when the lead screw is set on either plus or minus 16. Thus, the integrator constant of

Figure 10. Control cabinets which house the power contactors, the regulated d-c power units for the amplifiers, the motor-driven field potentiometers of the Ward Leonard generators, and the two Unis-motor speed-indicating instruments

these devices is 16. Similarly, there should be just twice as many revolutions of the output shaft as of the turntable shaft when the lead screw is set on plus or minus 32, and so forth. Any difference between these nominal "equivalent revolutions" is due either to mechanical errors or slipping of the integrator wheel or both. Since any slip present should be a function of speed and acceleration, it seems reasonable to assume that, if there is no appreciable variation in the equivalent revolutions throughout the speed and load range, any deviation from a 1:1 correspondence based on equivalent revolutions is due entirely to mechanical errors and not to slipping. The test results summarized in Table I indicate that slipping does not exist to a measurable degree in these integrating units.

Another test which affords a measure of the over-all back lash of the follow-up system and connecting gear trains, an important criterion of performance, is shown in Figure 9. This system of connections will be recognized as that required for the solution of the harmonic equation

$$\frac{d^2y}{dx^2} = -\frac{1}{K^2G^2}Y$$

in which K is the integrator constant and G is the gear ratio between the output shaft of each integrator. With one lead screw set on zero and the other at some arbitrary number of revolutions from zero at the start, if there were no back lash, the amplitude of succeeding cycles should equal this initial amplitude. However, because of the presence of back lash in the gears and follow-up system, the amplitude of successive cycles (number of lead-screw revolutions measuring displacement from zero) will increase. The rate at which this growth occurs in terms of revolutions per cycle is a measure of the over-all back lash and, therefore, serves as a good measure of performance. Typical sine-wave growth measurements of this nature are shown in Table II. Under the condition of high output-shaft speed and direct drive, the growth per cycle is most pronounced, as should be expected. As the step-down ratio is increased, the growth per cycle decreases rapidly and becomes relatively independent of integrator-wheel speed. The test results tabulated in Tables I and II were obtained with representative integrator units without the use of compensating means of any kind. When it is necessary to work with greater accuracies, suitable provisions can be made.

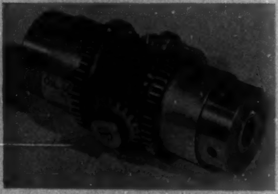

Figure 11. Representative adder unit

This low-friction design was found to require very small driving power

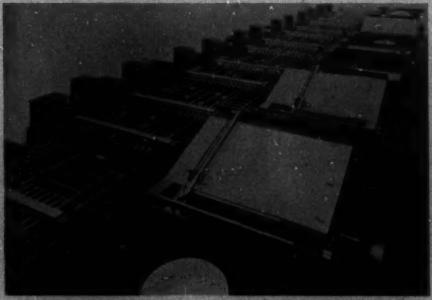

Figure 12. General view of the differential analyzer from the input- and output-table side

There are four input tables and two output tables. The input-table drives may be cranked in both the horizontal and vertical directions. All input tables have polar co-ordinate attachments

Control System

Speed control of the independent variable, ordinarily referred to as the time motor, is obtained by means of a Ward Leonard system which gives stepless speed control over the desired speed range, thus assuring the availability of optimum speed of operation at all times. The field potentiometer in the generator circuit is motor driven. This motor is controlled by means of push buttons at 12 convenient locations around the analyzer. A control panel is located at each input table and each pair of integrators. These panels include "start," "stop," "raise," "lower," and "reverse" buttons. They are so interlocked among the control stations that, if an operator at one station pushes a "lower" button, the time-motor speed will be decreased, even though operators at other stations may be pushing a "raise" button.

At each station there is also a two-position "hold" and "neutral" switch. If an operator cranking an input table requires that a certain time-motor speed not be exceeded, the switch is thrown in the "hold" position. The speed of the time motor cannot be raised then from any control station. It can be lowered, however, at any control station simply by pushing a "lower" button. Thus, the interlocking control system provides that the station requiring the slowest speed will always take control.

Whenever a "stop" button is pushed, and the time motor has come to a standstill, the motor drive for the generator field potentiometer automatically starts and brings the slider contact to the zero field potential position, thus assuring that, when an operator later pushes a "start" button, accelerations will not be excessive.

"Emergency stop" buttons are provided at convenient and readily accessible locations around the analyzer. By pressing this button all power to the analyzer, both alternating current and direct current, is removed. This serves to protect the machine against overspeed, and so forth, in case of a runaway or trouble, whatever its nature may be.

At each pair of integrators there is a push-button control for energizing the light source and tube filaments. This control has a time-delay relay which is interlocked with the a-c supply, so that voltage cannot be applied to the motor and thyratron circuits until five minutes have elapsed from the instant of energizing the filaments, thus assuring adequate heating time for the thyratrons.

Two separate time motors are provided, one at each end of the analyzer bed. This permits the simultaneous solution of two problems, provided that the total number of integrators or input tables required does not exceed those available.

The use of two time motors requires that the controls at each station be applicable to either of the two time motors, depending on which one is being used in conjunction with the integrators or the input table associated with that control station. In order to meet this requirement, all wiring for each time motor is in duplicate in the bed of the machine with two outlets at each control station, one for each time motor. By means of a jack-and-plug arrangement the control panel

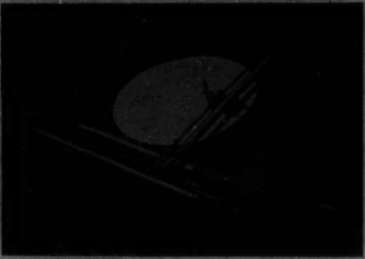

Figure 13 (left). Top view of input table showing the polar co-ordinate arrangement

Figure 14 (right). Top view of output table showing the ink pen and the clear ink records

Figure 15. General view of the differential analyzer

In the background are the two control cabinets, the wall-mounted master power switches, and the cabinet containing the induction-motor-driven Ward Leonard generators. The rack on the side accommodates all the accessories, such as gears, shafts, adders, and right-angle drives. The cabinet on the right of the accessory rack houses the air-conditioning system

Figure 16. Adjustable "dead-band" units

can be switched readily from one motor to the other as the requirements may dictate. This is shown in Figure 7, for example, applied to a pair of integrator units.

Figure 10 shows the control cabinets. They house the power contactors, the regulated d-c power units for the amplifiers, the motor-driven field potentiometers of the Ward Leonard generators, and the two time-motor speed-indicating instruments.

Miscellaneous Component Parts

A total of 20 differential gear assemblies or "adders" (one is shown in Figure 11) is available for use with the analyzer. These are of new design in which one of the major objectives was to keep friction to a minimum; this has resulted in light loads.

Helical gear boxes for right-angle drives are available. There are 50 right-hand and 20 left-hand drives.

Each input table (four are available) has both rectangular and polar co-ordinate drives. They can be used either as input tables for cranking in a function drawn in curve form, as manual multipliers, or as output tables for plotting solutions. Two output tables are located near the center of the analyzer. Each of these has two pencil carriages and will plot two functions simultaneously. The input and output tables are shown in Figure 12, and a close view of the input table arranged for polar co-ordinate drive is shown in Figure 13. Output-table records are obtained either by means of a fine-pointed brass stylus which is maintained lightly in contact with specially treated paper or by an inking pen. The ink records are easier to see and more permanent but do not permit as great accuracy in measuring. Figure 14 shows how distinct an inked output-table record may be.

Change gears totalling 440 are available in different tooth ratios required to give adequate flexibility to meet practically all needs.

The main bed of the machine is essentially the same as that for the analyzer at the University of Pennsylvania. It is approximately 32 feet long, and over-all width 9 feet. Fourteen steel pedestals support the analyzer. Each of these pedestals rests on the concrete floor to which all are bolted. Figure 15 shows a view of the complete analyzer.

The stub shafts were aligned by means of the screw supports on the bus-shaft housings. These screws are very helpful in initially aligning the shafts for minimum friction and binding. Once aligned, the adjustment remains stable, and further adjustment has been found unnecessary.

Two "dead-band" units (Figure 16) are available for the purpose of simulating intervals of no response or zero sensitivity, such as may be encountered in the solution of certain problems. Like those of the University of Pennsylvania, they are adjustable, so that the number of rotations of one shaft which can take place without the other one following it subsequent to a reversal of direction of rotation can be controlled. Once adjusted and inserted in the machine, the machine automatically includes the desired "dead band" in the solution.

Conclusion

Since the completion of this new differential analyzer several months ago, a number of important and urgent problems have been solved. The continuous

The MHO-Carrier Relaying Scheme

A Simplified Carrier Scheme for Long or Heavily Loaded Transmission Lines

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BECAUSE of the wartime problems of increased loads and shortage of materials, it is often necessary to put heavy loads on existing transmission lines rather than build new ones.

When transmission lines are heavily loaded it becomes difficult to retain relaying selectivity, because the currents and voltages which the relays measure are not sufficiently different to distinguish between faults and power swings except by adding auxiliary relays which check the rate of change of impedance. These auxiliary relays add complexity and cannot distinguish between power swings during which it is not desired to trip and an out-of-step condition for which it may be desired to trip.

This paper describes a new scheme which inherently distinguishes between the aforementioned swings and out-of-step conditions and, also, is very much simpler than its predecessors. It is based on the same principles as the "Long-line" relay scheme described in a group of papers[1,2,3] presented at the 1943 AIEE winter technical meeting, using phase-angle rather than impedance magnitude as a basis for distinction between load and fault conditions. It uses the same MHO unit, which measures admittance at about the phase angle of the protected line so as to provide a very high pickup on loads and power swings when set to operate on a fault at the end of the section. In this way the relays keep the transmission line in service as long as it can transmit power and remove it only when the systems swing apart so far that power cannot be transmitted.

Impedance Diagrams

In one of the aforementioned papers[2] it was shown that the locus of the impedance seen by a relay at a given location on a transmission line is a family of circles for different ratios of V_A/V_B where V_A and V_B are the terminal voltages at the ends of the line. The diameter of the circle tends to decrease as V_A/V_B departs from unity. It can be seen from Figure 1 that, if A were near the center of the system, V_A/V_B would decrease as the angle between V_A and V_B increased. Consequently, for a given system condition, the actual locus of the impedance seen by the relay at A during the progress of a power swing is a line joining the proper points of angular difference between V_A and V_B on the proper circles for varying ratios of V_A/V_B.

The difference between this new locus and the first family of circles is that the equivalent generator voltages E_S and E_R (Figure 1) are used instead of the line terminal voltages V_A and V_B. Since E_S/E_R usually is close to unity, the new loci will be circles of large diameter for different values of E_S/E_R; hence, usually it is permissible to dispense with the complex calculations of the rigorous method, and to consider all of the circles to be a single straight line passing through a point on the line-impedance vector separated from the origin by a distance approximately equal in ohms to the distance of the relay location from the electrical center. This is equivalent to assuming that the ratio of the equivalent generator voltages E_S/E_R is unity, and that there is no charging current or shunt loads. These simplifications have been used before,[4,5] and result in the following relatively simple determination of the power-swing locus on an impedance diagram. They are permissible here, because the

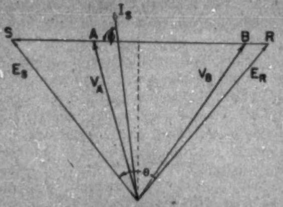

Figure 1. Vector diagram of a simple power system

Paper 44-46, recommended by the AIEE committee on protective devices for presentation at the AIEE winter technical meeting, New York, N. Y., January 24–28, 1944. Manuscript submitted November 10, 1943; made available for printing December 13, 1943.

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availability of this analyzer will result in greater knowledge and better understanding of many other technical problems, solutions of which heretofore have been possible theoretically but impossible practically.

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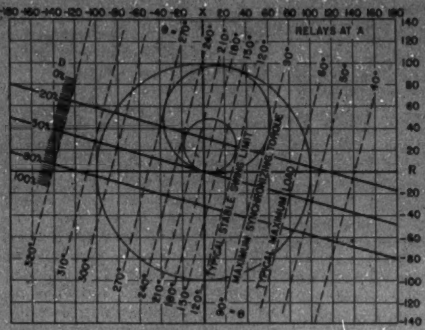

$X/R = \tan 75°$
No shunt load
Shunt capacitance neglected
D = distance of relay location from equivalent internal voltage of the leading end E_S, in per cent of total impedance from S to R
The co-ordinates are scaled in per cent of total impedance from S to R; abscissas are resistance, ordinates are reactance

Figure 2. Impedance diagram for relays at various locations along a line connecting two systems, or machines, applicable to the special case in which $E_S = E_R$

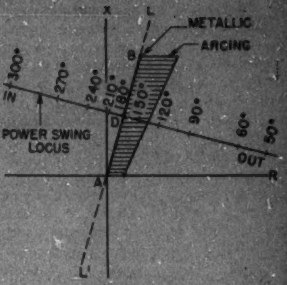

Figure 3 A. Resistance–reactance diagram of faults and swings

In = power incoming to bus
Out = power outgoing from bus

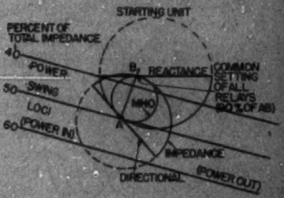

Figure 3B. Characteristics of relays plotted on the R and X diagrams of a simple power system

possible inaccuracy is substantially tangential to the characteristic of the new relay and, hence, do not affect its setting.

The construction for obtaining the straight-line locus is as follows. Figure 1 is a simplified vector diagram of power transfer through a transmission line; it assumes that the power system has a uniform distribution of R and X and neglects capacitance. S–A is the impedance drop of the system behind the bus at the sending end of the line and R–B is the drop behind the bus at the receiving end. E_S and E_R are the sending- and receiving-end voltages which are assumed equal, and θ is the angle between them. I_S is the load current, and ϕ is the impedance angle of the transmission line. The input impedance at a point A is V_A/I_S which will change as θ changes. If V_A/I_S is plotted on an R and X diagram, it will appear as a straight line at right angles to the line impedance intersecting it at a distance from the origin equal to the distance of the point A from the electrical center. The vectorial equation of this line in terms of the angle θ between the generated voltages at the ends of the system is

$$\boldsymbol{Z}_A = \frac{\boldsymbol{Z}_S}{2}\left[(2D-1) - j\cot\frac{\theta}{2}\right]$$

where D is the per unit distance of A from the leading end of the system (see Figure 2), and Z_S is the total impedance between the generator voltages E_R and E_S; $\boldsymbol{Z}_A$ and $\boldsymbol{Z}_S$ are vectorial quantities.

When faults are plotted on a similar R–X diagram along with the power-swing characteristic we obtain Figure 3 A. A fault appears instantly as a point on the line characteristic LL'. This line is at the angle $\phi = \tan^{-1} X/R$ where X and R are the reactance and resistance of the transmission line. Point B is at a distance from the origin equal to the impedance of the line section being considered. Point B is for a solid metallic fault at the end of the section and, since most faults are flashovers and involve arcs which introduce a resistance, this is represented by adding such a component horizontally, the shaded area being the fault zone.

Loads, power swings, and out-of-step conditions are represented as impedance having different R and X values which move inward along a line at right angles to LL' and cut it at a point D such that AD is approximately the distance of the relay location from the electrical center. The values of θ are marked along the power-swing line and it can be seen very quickly what degree of system separation appears as the same impedance value as a fault.

When the characteristics of the different types of ohmic relays are plotted on an R–X diagram, Figure 3 B is obtained, which shows that, for a given ohmic setting (90 per cent of line length AB) the reactance relay operates most readily, the impedance relay less readily, and the MHO unit much less readily than either. It can be seen that the MHO unit is simply a starting unit set for first-zone reach instead of third-zone reach.

When used with carrier blocking, however, it is apparent from Figure 4 B that the tripping area of the reactance relay is restricted considerably on power swings; out-of-step blocking, of course, prevents the distance elements from tripping in the horizontally shaded areas. In both cases, the electrical center has to be within the protected section to cause tripping, and tripping will only occur if the power-swing locus enters the tripping zones of the relays at *both* ends of the section. The carrier tripping area is shown shaded vertically in Figures 4 A and 4 B for the impedance and reactance relays. These diagrams are to the same scale, and illustrate that the tripping area is somewhat larger for the impedance relay because the radius of the characteristic determines the distance reach in the impedance relay, whereas only the diameter is involved in the case of the starting unit of the reactance relay.

Present Distance-Relay Carrier-Current Systems

It is apparent from Figures 4 A and 4 B that both of the present distance-relay–carrier combinations may limit unnecessarily the power that can be transmitted over a transmission line. The reactance relay limits this power by virtue of the fact that the starting unit must reach as far as is desired for the third-zone time step. The impedance relay, on the other hand, limits the load transfer because of the second-zone impedance unit, a type of relay unit that embraces more area on the impedance diagram than necessary.

The fundamental basis of the relay system herein described is the use of the starting unit of the reactance relay, but with its ohmic reach reduced to slightly greater than the line impedance, thereby encompassing a minimum area of the impedance diagram. A third-zone backup step may be obtained, if desired, by means described later.

To many operators, the principal objection to present-day distance-relay–carrier schemes is their complexity. The relay system to be described eliminates a substantial portion of this complication by establishing a group of "building blocks" which provides, in most applications, a simple relay scheme. For the more difficult applications, additions may be made without changing the fundamental relay scheme.

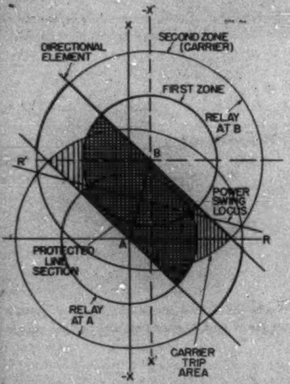

Figure 4 A. Impedance-relay characteristics plotted on *R* and *X* diagram so as to show combined tripping area

Vertically shaded area = carrier trip
Horizontally shaded area = distance trip

Requirements of the Ideal Scheme

Most of the requirements have been stated already in the previous paper on long-line relays.[2] A more complete list is as follows:

1. Trip instantaneously for internal faults, arcing or solid, including three-phase faults at the circuit-breaker terminals.
2. Trip in delayed time for external faults.
3. Provide carrier blocking for external faults.
4. Not operate relays or carrier on loads up to the steady-state power limit.
5. Control tripping on system oscillations according to a value of impedance, seen by the relay, which represents the maximum power-swing condition for which it is desirable to stay in.
6. Be flexible enough to provide blocking on out-of-step conditions which may make this desirable.
7. Clear instantaneously all internal faults occurring during power swings or immediately after an external fault. Trip after a time delay if out-of-step blocking is used.
8. Allow reclosing after tripping on a fault, but not after tripping on an out-of-step condition.

The value of impedance representing item 5 depends upon either the maximum angle which can be tolerated from a stability standpoint or the angle which can be tolerated because of other limiting system conditions, such as low voltage.

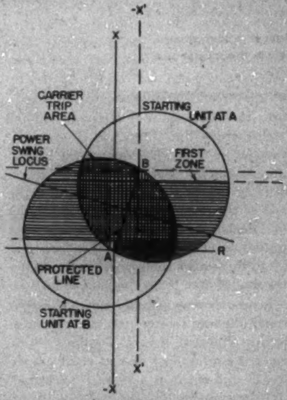

Figure 4 B. Reactance-relay characteristics plotted on *R* and *X* diagram so as to show combined tripping area

Items 1, 3, 4, 5, and 7 are inherent in this scheme; item 2 may be achieved by backup relays as discussed later, and item 8 is accomplished by using the rate of change of impedance for blocking the reclosing circuit instead of the tripping circuit. A more detailed explanation of the operation of the relays now follows.

Characteristics of New Scheme

The original carrier-current idea[6] was a very simple one, consisting of only a pair of directional relays which started carrier for external faults and made up a trip circuit for internal faults. This scheme returns to that idea in that it has but two relay elements—one for starting carrier, and one for the trip circuit.

The basic circuit is shown schematically in Figure 7. It will be realized, of course, that certain liberties have been taken in order to present the circuit in its simplest form for purposes of explanation.

The unit *Z* starts carrier for all phase faults by opening the carrier-start lead, and the unit *M* turns off carrier if the fault current is flowing into the section and completes the trip circuit up to the receiver-relay contacts. Then the removal of carrier current from the receiver relay allows it to trip the breaker.

A ground fault in the reverse direction opens the contacts GD_1, starting carrier and causing the contacts *R* to remain open and prevent tripping. A fault in the tripping direction closes the contacts GD_2 to complete the trip circuit up to the receiver relay, which trips because no carrier is started.

The connections actually used are illustrated in Figure 8, and the operation of the relays is described in the following section.

Operation

A. PHASE FAULTS

Each phase has an impedance relay for initiating the carrier signal under abnormal conditions by circuit-opening contacts Z_1, Z_2, and Z_3, Figure 8. This characteristic is shown as the circle *Z* in Figure 5.

An impedance element was selected for starting carrier because it will operate on power swings as well as faults. It has

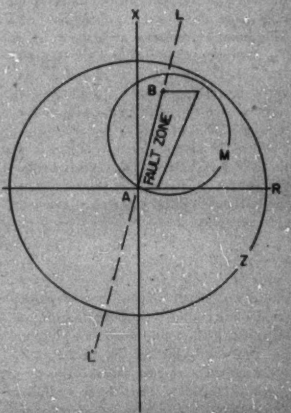

Figure 5. Characteristics of the MHO-carrier relaying scheme

M defines the tripping zone; *Z*, the carrier-starting zone

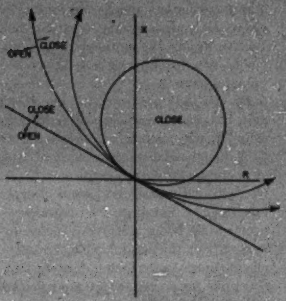

Figure 6 A. An impedance diagram illustrating how the straight-line characteristic of a directional relay becomes a circle as voltage restraint is added

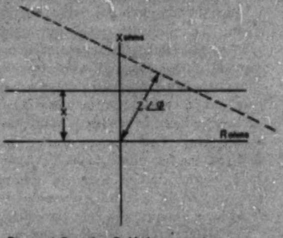

Figure 6 B. An R–X diagram of a reactance unit ($Z\underline{/90^\circ}$) and an ohm unit ($Z\underline{/\phi}$)

Reactance unit—solid line
Ohm unit—dashed line

the advantage of being available, on medium-length lines, for preventing tripping or reclosure on out-of-step conditions. It can also be used for controlling a timing unit for backup purposes if the stepped distance-type backup is desired.

The second, or tripping, element in the relay scheme is an admittance element, called, for short, a MHO unit, since admittance is expressed in mhos; its characteristic is shown by circle *M* of Figure 5. This unit has been known for many years as the starting unit of the reactance-type distance relay.

A MHO unit is a directional element with voltage restraint added. As a directional element, it has maximum torque at some particular angle. As such, its characteristic is a straight line, as in Figure 6 A, operating to open contacts to the left of the line and to close them to the right. The addition of voltage restraint curls up the ends of the straight line into a circle, the size of which becomes smaller as the restraint is increased.

Each phase has a MHO unit, M_1, M_2, and M_3, which stops carrier through an auxiliary relay *MX* and makes up the trip circuit through a carrier-current receiver relay *R*, if the fault is within the protected section. In this respect the scheme is similar to earlier equipments.

B. GROUND FAULTS

The ground relaying has been considerably simplified by a return to the original idea of back-to-back directional relays, which is permissible for ground relays because there is no complication of power-swing currents entering at right angles to the fault currents. There is a complicating factor in the ground relaying, however, namely, the now known fact[7] that residual currents can appear in the current-transformer circuits for phase faults not involving ground. These undesirable stray currents occur because the current transformers do not balance out perfectly, and this, in turn, is primarily the result of premagnetization of the core, not to mention differences in manufacturing tolerances, et cetera. The usual settings require current transformers to be within less than one half of one per cent of each other, which is a severe requirement.

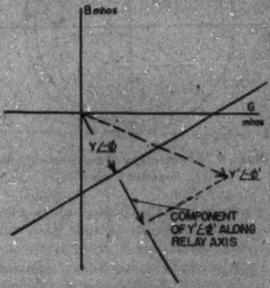

Figure 6 C. A G–B diagram of a MHO unit ($Y\underline{/-\phi}$)

The use of a directional relay for starting carrier requires simultaneous errors of the proper phase angle of the polarizing source, as well as of the line current transformers, before a false operation can occur, since it operates on a product of the residual current of the three line current transformers and the polarizing voltage or current. With directional fault detectors throughout, no preference need be given to either the ground or phase relays. The idea of preference arose originally because the ground relaying had to be more sensitive than the phase relaying, and it was feared that load or power-swing currents would take control unless the ground were given preference. It has been dispensed with now, because years of experience have indicated that false residual current may be produced by magnetized current transformers during phase faults, which may operate the ground overcurrent relay which turns on carrier and prevents tripping.

Carrier is started by a ground directional relay GD_1 (Figure 8) with normally closed contacts, which open for faults in the backward direction and remain closed for faults in the forward, or tripping, direction. The trip circuit is made by a similar directional relay GD_2 connected just oppositely with normally open contacts.

Settings of the Z and MHO Units

The impedance relay may be set to reach beyond the end of the line as long as it does not exceed the impedance value (see Figure 2) corresponding to maximum steady-state load. On short lines there will be no possibility of this, but on very long lines it is advisable to confine the relay setting to the line-section length, in order to avoid having carrier on for long periods. Reference to Figure 2 will show that, if the line could contain all the system impedance, the relay characteristic would reach to some point between the 50- and 60-degree lines which represents about the maximum separation that would be considered for steady-state conditions.

The MHO unit controls tripping, and, therefore, should not operate on power swings from which the system can recover. As stated before, the actual ohmic value representing this borderline depends upon system conditions, but it is known that above 90 degrees difference between the equivalent generator voltages the synchronizing power will decrease so that, while they may swing almost 180 degrees apart and still be stable, the limit is some value nearer 90 degrees.

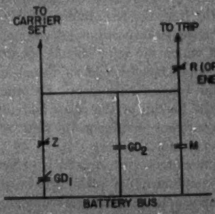

Figure 7. Simple elementary diagram of the MHO-carrier relaying scheme. The carrier starts when the circuit is opened and stops when the circuit is closed

See subcaption of Figure 8

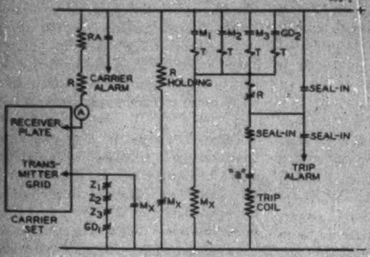

Figure 8. Complete elementary diagram of the MHO-carrier relaying scheme

A = ammeter
GD_1 = ground directional relay (blocking)
GD_2 = ground directional relay (tripping)
M_1, M_2, M_3 = MHO relay units in phases 1, 2, and 3
MX = MHO auxiliary relay
R = receiver relay
RA = receiver alarm relay
T = target
Z_1, Z_2, Z_3 = impedance-relay units in phases 1, 2, and 3

With all the system impedance in the line, if the MHO unit has maximum torque at the same angle as the line and is set to just cover the line, it will reach about the 90-degree separation line; if the line impedance is half the total impedance, it will reach to some point between the 120- and 130-degree lines. It can be seen from this that, on short lines and most long lines, the MHO unit will trip for faults and out-of-step conditions, but not power swings. On very long, very heavily loaded lines, some form of "blinders" will be required, such as the ohm units used in the long-line scheme,[1] which measure the component of impedance along the locus of the power swing so as to prevent tripping below a certain angle of system separation determined by a system study.

It will be seen from the foregoing that an application engineer can determine relay settings readily, as soon as he has decided the limits of steady-state load and the maximum angle permissible for power swings.

Description of Relays Used

A. The Phase Relays

Impedance Fault Detector. The characteristics of this relay often determine the maximum length of line that can be protected with this system of relaying. The longer the line, the less difference there will be between the impedance of a remote fault for which the relay should operate and the impedance for load conditions for which the relay should reset. In other words, the pickup of the relay and the dropout current should be as near together as possible.

The impedance unit Z used is of the induction-cylinder-type construction, using all eight poles to obtain a torque proportional to the operating current. This torque is perfectly smooth, free from any 60-cycle vibration—an inherent feature in the induction-cylinder construction.[8] This feature is necessary because the unit may remain in the operated position for considerable periods of time when the systems are exchanging power at near the stable operating limit. Voltage restraint is provided by an attracted-armature-type construction with an adequately smoothed pull. This type of construction gives the most force for the least volt-ampere burden—practically a requirement for an impedance relay. Such a relay is seriously limited in this respect because the potential coil must stand rated voltage continuously and yet, when it is called upon to operate, must do so with only a small percentage of normal voltage. The usual disadvantage of an attracted-armature construction is that the force diminishes rapidly as the gap increases, being higher than the second power of the gap. This is minimized by operating with an air gap several times as large as the contact gap, and by using the minimum number of contacts; namely, a transfer contact giving one normally closed and one normally open contact. By this means, the unusually high dropout of over 90 per cent of pickup is obtained, Figure 9.

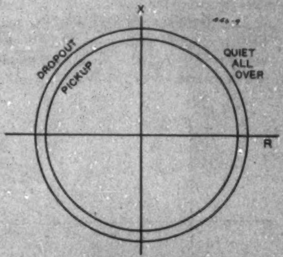

Figure 9. Characteristics of induction-cylinder impedance relay

The MHO Tripping Relay. The MHO unit has the same four-pole induction-cylinder construction employed in the *GCX* directional distance relay, producing an operating torque proportional to $EI\cos(60°-\theta)$ and a restraint torque proportional to E^2. The angle of 60 degrees was selected as being near, but generally smaller than, the natural angle of a transmission line in order to minimize the tripping zone for power swings as explained under "Operation."

One of the most important features of the MHO unit is its memory action. No directional relay is expected to function with zero voltage, and this is the condition at which the mho circle passes through the origin. This difficulty is circumvented by making the polarizing potential circuit resonant at 60 cycles, so that the coil continues to have a substantial voltage across it for several cycles after the line voltage has been reduced to zero by a fault close to the bus. This feature cannot be shown on the two-dimensional R–X mho circle diagram, but, if polarizing torque were plotted in a third dimension, it would have a sharp peak near the origin.

The MHO unit has double-wound current coils which give the same reach on phase-to-phase as on three-phase faults.

Origin of the Term MHO Unit. Why is the unit called a MHO unit? This sounds complicated, but we believe that it is actually the simplest name possible. The characteristic of the MHO unit is the circle of Figure 6 A, and whereas this shows graphically where the relay operates, there is no expression which states what the relay operates on. For 15 years we have had a reactance ohm unit which has the characteristic of the horizontal straight line in Figure 6 B. This unit operates whenever the reactive component of the impedance, measured in ohms, is less than its setting X. This character-

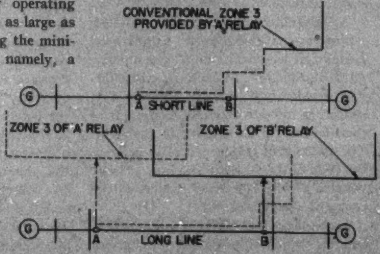

Figure 10. Tripping zones illustrating conventional and nondirectional long-line backup

istic can be tilted as shown by the dotted line, and then the unit operates whenever the ohmic component along the perpendicular line through the origin is less than the setting of $Z/\underline{\phi}$. Now, the MHO-unit characteristic is a straight line when plotted on an admittance diagram, as in Figure 6 C, and this immediately shows that it operates whenever the component of the admittance Y at the angle ϕ is greater than the relay setting.

B. GROUND RELAY

The ground directional units are likewise of the induction-cylinder construction available with either current or potential polarization. Their torque is proportional to the product of the two currents or to the current and the voltage, so that false operation would require an error in the polarizing source as well as a false residual current resulting from unequal current-transformer action. The lack of response to the d-c component of offset waves is an already well-known feature of the induction principle.

C. RECEIVER RELAY

A conventional permanent-magnet polarized receiver-relay unit is employed. It has spring-closed contacts and two restraining windings, one energized from the local battery, and the other energized by carrier received from either end of the line.

Usually, the first winding is energized through back, or normally closed, contacts of the mho-auxiliary unit *MX* in Figure 8, and holds the receiver-relay contacts open. When a fault occurs, the fault detectors turn on carrier, energizing the second winding by the carrier-receiver plate current. If the fault is within the protected section, the phase or ground directional relay *M* or *GD* shuts off carrier and de-energizes the battery winding so that the spring-operated contacts of the relay close and trip the breaker.

The advantage of this arrangement is that the second winding, the so-called d-c holding coil, keeps the contacts open until the trip circuit is completed. This coil is energized for reverse faults, thereby providing double assurance of preventing tripping.

Backup Protection

Backup protection is being considered separately because opinions differ as to the best type of relays, but almost everyone agrees that the backup relays preferably should be self-contained and separate from the carrier relays.

This is most easily achieved with the ground relays for which the standard residual product or directional residual-overcurrent relays are very satisfactory. In the case of the phase relays, either directional overcurrent relays or stepped distance relays can be used, but in either case a MHO unit should be used for directional purposes, except in the case of very long lines on which the 20-degree ohm-unit blinders[1] are used.

The standard two- or three-step distance characteristic can be provided, though with some loss of simplicity of the scheme. The first step may be a standard reactance ohm unit set for 90 per cent of the protected section, using the MHO unit of the carrier-relay group as a starting unit; or it may be an entirely separate MHO unit, set for 90 per cent of the line and connected directly to the trip coil so as to provide a very reliable emergency trip of the utmost simplicity. The chief purpose of the zone-1 distance step is to provide protection when the carrier equipment is out of service for maintenance.

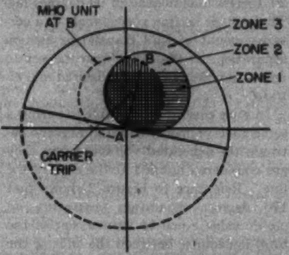

Figure 11 A. Conventional zone-3 backup for a short or medium-length line showing zones of protection provided by breaker *A*

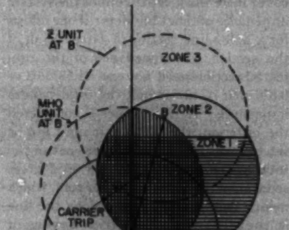

Figure 11 B. Nondirectional zone-3 backup for a long line, showing zones of protection provided by breaker-*A* relays and supplemented by breaker-*B* relays

Note that the zone-3 circle is the same size as Figure 11 A, which is determined by the maximum-load transfer. All the enclosed area represents protection

In an emergency, instantaneous tripping on internal faults can be obtained while the carrier is out of service simply by short-circuiting the receiver-relay contacts with the carrier cut-off switch in the "off" position. This will enable the MHO unit to give instantaneous protection with carrier off; the fact that it is set for 110 per cent of the line ohms may not be as important as it sounds because, if there is generation on the bus, the actual reach in miles may be only one or two per cent beyond the end of the section, and the chance of a trip on an external fault in that small section would be very remote. If immediate reclosing is available, its significance almost disappears.

The second and third zones can be provided by a timing unit controlled by the impedance relay. The zone-2 contact is connected in parallel with the receiver relay so that, if the latter does not operate, such as when the fault is just beyond the bus but within the mho-unit range, the breaker will be tripped after a short delay.

On very long lines it is difficult to make the third zone reach far enough to do much good as backup, particularly if there is generation at the next bus. In such cases the relay nearer the fault can be used for zone 3, as shown in Figure 10, so that the voltage restraint on the relay is less by the drop in the line. The upper part of Figure 10 shows the usual arrangement of the time steps of distance relays and the lower part shows how the third step can be provided by the relay near the fault. The appearance of the relay characteristics to achieve this result are shown in Figures 11 A and 11 B, respectively.

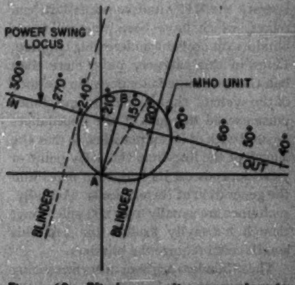

Figure 12. Blinders permit power swings to enter the MHO-unit circle

Note that the 60-degree MHO circle requires but one blinder on a 75-degree line

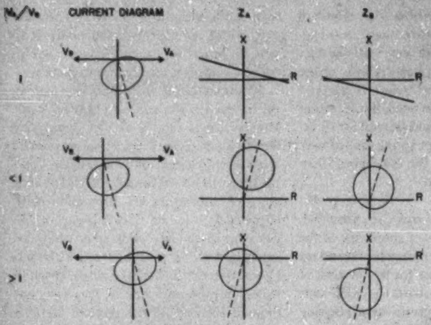

Figure 13 A. Characteristic loci of impedance as seen by relays during a power swing for different ratios of V_A/V_B

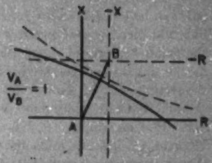

Figure 13 B. Locus of swing impedance viewed from the two ends of a transmission line

The solid line is seen from *A* and the dashed line from *B*

This simply means having the third-step timer contact going directly to the trip circuit and not controlled by a directional unit. The range of the third step, therefore, will be the ohmic setting of the impedance relay, which, on a long line, will be something less than the length of the setting. Thus backup time will be provided for external faults behind the bus and, also, over part of the protected section.

Very Long Lines

Figure 2 shows that the impedance seen by the relay during a swing decreases as the systems get farther apart until a point is reached where the relay characteristic is crossed and the tripping occurs. Since the size of the relay characteristic increases with the length of line covered, it can easily be seen that the tendency of the relays to trip during power swings increases with the length of line and the magnitude of the power swing.

Lines which are so long, or so heavily loaded, or both, that the line impedance during power swings is low enough to operate the MHO unit, require additional relays, which have been referred to as blinders, to prevent undesirable tripping. Lines in this category have more than half the total impedance between the ends of the system; that is, the line impedance must exceed the sum of the impedances beyond each of its extremities, so that the length of the line from the relay point of view depends on its length in miles and the generation at its terminals. Actually, such lines are usually over 200 miles long, though a heavily loaded line half this length could require the blinders.

These blinders are ohm units measuring a component of impedance along the power-swing locus, that is, about at right angles to the line characteristic.

These units have been described elsewhere in detail.[1] They are similar to the standard *G-E* reactance-relay ohm unit except that they measure impedance at 20 degrees lead instead of 90 degrees lag. Their ohmic setting corresponds to a certain amount of load transfer or system separation, so that they may be set to trip on a power swing of a desired magnitude.

In some cases it is desirable to permit a separation of more than 120 degrees between the generated voltages when they are considered lumped at the ends of the line. Reference to Figure 2 shows that 120 degrees separation represents an ohmic value equal to 29 per cent of the total impedance between the ends of the system, which means that a MHO unit with maximum torque at the same angle as the line phase angle must not be set to cover more than 58 per cent of the system impedance without blinders; if one allows for a MHO-unit setting of 110 per cent of the section, the impedance of the protected section must not exceed 58/110 = 0.52, that is, about half the total system impedance. If 150 degrees is considered the criterion, the MHO unit must not be set to cover more than 28 per cent of the total system impedance. For 90 degrees separation the figure is 100 per cent, which means that, no matter how long the line is, the MHO unit will not trip on swings or load conditions causing a system separation of less than 90 degrees. It should be remembered, however, that Figure 2 assumes straight lines for power-swing loci, whereas they are generally curves and variable, so that these percentage values are only approximate.

The corresponding figures for the impedance relay are half those of the MHO unit because the radius of the impedance-unit circle gives the same ohmic, or distance, setting as the diameter of the MHO unit. In other words, the impedance unit must be set for not more than half the ohmic reach of the MHO unit in order to avoid operation on swings.

The minimum ohmic setting of the blinders is the maximum arc resistance expected. This can be understood readily by reference to Figure 3 A. The maximum arc resistance[9] is 8,750 $L/I^{1.4}$ where I is the current in amperes and L is the arc length in feet. For a 154-kv line the spacing will be about 14 feet and, if allowance is made for a light wind, the arc length may be 20 feet; at 500 amperes minimum current this would give an arc resistance of $8,750 \times 20/6,000 = 29.2$ ohms. Compared with this, a 100-mile line would have an impedance of about 80 ohms, but, of course, at a highly lagging angle.

It is sometimes unnecessary to use two sets of blinders, that is, six ohm units for preventing tripping or power flow in either direction. Usually only one set of three ohm units is required because it is generally possible to have the angle of the MHO unit less lagging than that of the line, so that the relay may be vulnerable only in the direction of outgoing power, as shown in Figure 12.

Reclosing

Many of these long and/or heavily loaded lines are the lines between systems or parts of a system whose loss may result in the loss of other ties. Hence instantaneous reclosing after clearing faults may often be the means of saving the stability of the system.

It is preferable, however, to reclose only after faults and not after tripping on power swings or out-of-step conditions, because in the latter case the system is liable to be too far apart to stay in when reclosed. When a swing occurs it is caused by an abrupt change in load, generally an increase in load due to the loss of a source of generation. Reclosing is undesirable if the new load is greater than the load that can be reclosed successfully, that is, with stability after reclosure.

This selection of conditions suitable for reclosing therefore can be made on the basis of the ohms along the power-swing locus representing the maximum steady-state load. A blocking relay with this ohmic setting can pick up an auxiliary relay with a slight delay which will prevent reclosing unless it is de-energized by the tripping relay before the auxiliary has had time to pick up, as would be the case of a fault where the blocking and tripping relays would operate simultaneously.

On short and medium-length lines the impedance relay that starts carrier can be used, also, to energize the auxiliary relay for blocking reclosure, because the impedance relay can always be set higher in ohms at any angle than the MHO unit which controls tripping and which can be used, also, for preventing the blocking.

On very long lines the impedance unit may not pick up before the MHO unit, and, in fact, both may be rendered ineffective by blinders. In order to ensure proper operation, in such cases, an additional single-phase unit is required to pick up the auxiliary relay and it must be of the same type as the tripping relay but with a higher ohmic setting. If the tripping relay is a MHO unit, the blocking relay must also be a MHO unit, so that it will pick up before the tripping MHO unit under all conditions. Similarly, if tripping is limited by a blinder, the blocking relay also must be an ohm unit like the blinder, but set for higher ohms. There is always a margin between the two like units, because the tripping unit must be set below the ohms of the maximum power swing, whereas the blocking unit has only to clear steady-state load conditions.

Where stepped-distance backup is used, the distinction often can be made very simply by arranging to reclose only after tripping by carrier relays. The line must be short enough to permit the impedance unit to be set farther than the MHO unit, so that carrier blocking will be available on all power-swing conditions where the electrical center is within the protected section. In that case power swings will trip through the first-step MHO or reactance unit and will not reclose, whereas a fault will energize the carrier-relay trip circuit, also, and operate an auxiliary relay which will initiate reclosing.

Conclusions

The new scheme has a direct approach to the problem of relaying heavily loaded transmission lines, and has the virtue of considerable simplification over previous schemes.

It is applicable to all lines which normally would be considered long enough to justify the cost of carrier relaying.

Appendix

The following is a pictorial explanation of why the impedance loci appear as circles for a given ratio V_A/V_B. It has been explained already under "Impedance Diagrams" how the straight lines in Figure 2 are obtained for the condition $E_S=E_R$ and no shunt impedances. Figure 13 A shows a series of diagrams representing the loci Z_A and Z_B at stations *A* and *B* in Figure 1, *A* being at the leading, or sending, end of the transmission line *AB*.

The current diagrams[4] would be circles if all the impedance were in the line, that is, if bounded by infinite busses. As the relay location approaches the electrical center the current locus approaches a semicircle. The voltage locus, however, has a complementary shape, so that the impedance becomes a circle.

In these diagrams capacitance has been neglected, but it can be seen easily that capacitance will tend to curl the straight lines for $V_A/V_B=1$ downward, because they would tend to pass through a point on the $-X$ axis when V_A and V_B were in phase, so that the only current was charging current. Hence the effect of charging current would be to make all the circles below the *R* axis smaller and those above it bigger. For that reason the circles Z_A and Z_B would not be exactly symmetrical with respect to each other. In other words, if Z_A and Z_B were plotted on the same diagram, as in Figures 4 A, 4 B, and 13 B, the two loci would not coincide because of the effect of capacitance.

In the preliminary diagrams of Figure 13 A, small circles are shown because V_A/V_B and V_B/V_A are large. Actually, the ratio would seldom exceed 1.25 and the circles would have a very large diameter, so that the part in the region of the power swings and the relay characteristics is more nearly a straight line.

A great deal of work has been done on graphical and analytical methods of obtaining the power-swing impedance loci. A quick method[10] is to obtain the line impedance in the form of the equivalent *T*, and to calculate directly from it the radius and the ordinates of the center of the circle for the required ratio of V_A/V_B by vector algebra.

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Effect of Altitude on Impulse and 60-Cycle Strength of Electrical Apparatus

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ALTITUDE is accompanied by a lowering of the air density, and, accordingly, it affects the performance of electrical apparatuses which depend on the air for operation. Thermal performance is affected because of the reduced heat dissipated by convection. Similarly the dielectric strength suffers a decrease with lower air density. The windage losses of machines and damping of instruments are less, but, in either case, the amount is not of consequence. At very high altitudes commutation may present a major problem and storage batteries become affected adversely. The interrupting capacity of some circuit-interrupting devices is also affected, the amount depending on various conditions. These are well-known instances where altitude is a factor in the operation of electrical apparatus.

Much research activity has been directed to these problems. From the beginning of high-voltage transmission, altitude was recognized as a factor affecting the operation of line and station apparatus.[1] Repeated efforts have been made to determine corona loss, and, to this end, several investigations have been conducted on transmission lines at high altitude.[2–5] Contributions on the dielectric strength of air[6,7,4,] and of electrical apparatus[8] at normal and low air densities date from this early period. Similarly the effect of low air density on the thermal characteristics of electrical apparatus has been the subject of comprehensive investigations both from an engineering[9–14] and a fundamental approach.[15,16] The physical aspects and other practical considerations of the problem[17–21] have been examined from time to time. As far as feasible the results of this work have found expression in the various apparatus standards and test codes.

Certain fundamental data are still lacking although much progress has been made. This fact has been brought out in the Pacific Coast AIEE conference on the "Effect of Altitude on Rating, Testing, and Operation of Electrical Apparatus" in conference papers presented by P. L. Bellaschi, L. W. Robertson, and V. M. Montsinger and elsewhere.[22] For instance, an immediate need is for impulse and 60-cycle data on actual station and line apparatus at low air densities corresponding to high altitude. This need has been recognized for some time, and the results of this investigation should therefore be timely and helpful in the field of application at high altitude.

Test Setup and Apparatus Tested

The tests were made in a sealed Micarta cylinder, 30 inches in diameter and 60 inches long inside. The test setup is shown in Figure 1. Either end of the cylinder may be removed, the apparatus for test enclosed, and the vacuum drawn. At the desired air pressure, voltage is applied to the high-voltage terminal which enters the cylinder at one end, the second terminal at the other end connecting to ground. A valve is set to leak air into the cylinder at a rate equal to the air removed by the vacuum pump; thus the air circulates through the cylinder and the inside pressure is maintained constant. Instruments give the essential data on the air. A barometer and a manometer provide measurements of the air pressure in the cylinder and outside. Wet and dry bulb thermometers measure the humidity and a thermocouple thermometer the temperature in the cylinder.

The tests cover some of the more common apparatus encountered in stations and on lines. These are listed in Table I. Figure 2, for instance, shows 15-kv and 69-kv bus and switch insulators typical of the standard apparatus that have been tested. Figure 3 illustrates a six-unit insulator string in the process of being lowered in the cylinder for test. The arrangement in Figure 4 for enclosing a 69-kv bushing indicates the flexibility of the Micarta cylinder in conducting the tests on the various station and line apparatus. In part of the tests as on the wood crossarm, shown in Figure 5, a porthole window was available at the ground end of the cylinder for visual observation of corona, flashover, and other effects. The smaller apparatuses were fitted with terminal electrodes in the manner prescribed in the usual dielectric tests. For large apparatus the clearance was limited, but in no instance did flashover occur other than over the apparatus or in the air. Thus the tests truly and effectively were on the apparatus proper in a reduced atmosphere.

Method of Test

The procedure was to adjust the air density in the cylinder to the desired value and to condition the apparatus and the cylinder to the "new altitude." We found that for most apparatus the time required for the pumping sufficed to

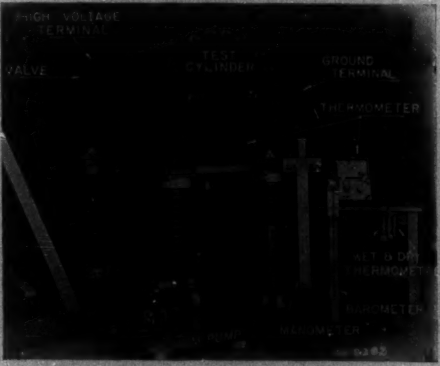

Figure 1. View of test setup

Paper 44-67, recommended by the AIEE committees on electrical machinery and power transmission and distribution for presentation at the AIEE winter technical meeting, New York, N. Y., January 24–28, 1944. Manuscript submitted November 13, 1943; made available for printing December 22, 1943.

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Table I. *RAD* Investigation of Station and Line Apparatus

Apparatus Tested	Type	Impulse Tests				60-Cycle Tests
		1½x40 Microseconds		1,000 Kilovolts Per Microsecond		
		Positive	Negative	Positive	Negative	
15-kv switch and bus insulator	Station (outdoor)	X	X			X
69-kv switch and bus insulator	Station (outdoor)	X	X			
69-kv condenser bushing	Station (outdoor)	X	X			
6-unit 10-inch-diameter suspension insulator	Station and line	X	X			
15-kv dry-type current transformer	Station (indoor)	X				
15-kv apparatus insulator	Station (indoor)	X	X			X
15-kv air-insulated transformer (Model)	Station (indoor)	X	X			X
8.66-kv air-insulated transtransformer (Model)	Station (indoor)	X	X			X
60-kv crossarm, four-foot section	Line	X	X			
6- and 12-inch rod gaps	General	X	X	X	X	
24-inch rod gap	General	X	X			
½-inch transformer oil (gap)	General		X			

condition the apparatus and the cylinder. In some tests, as on the transformer oil, constant air density was maintained several hours previous to the test. As stated before, a leak valve in combination with the vacuum pump provided a continuous flow of the air at constant pressure in the cylinder.

Tests were made from normal to low air densities and vice versa, with practically the same results. Most test data are limited from 1.00 to 0.50 relative air density (*RAD*), as this covers the practical range in altitude encountered on the earth's surface (Table II). However, some tests were extended below 0.10 *RAD* which corresponds to well above 40,000 feet and a few up to 1.50 *RAD*. All tests were made at room temperature, 20 to 30 degrees centigrade, and at 890 feet (Sharon, Pa.).

The data are reported in terms of the relative air density, given by the formula

$$RAD = \frac{0.392\ b\ \text{(millimeters)}}{273 + t\text{(degrees centigrade)}}$$

in which b is barometric pressure and t is temperature. The absolute humidity at the low *RAD*'s is, essentially, the absolute humidity at atmospheric pressure reported for each series of tests multiplied by the *RAD*. The reason for this lies in the fact that for a given mass of air, the water content is the same inside and outside the cylinder.

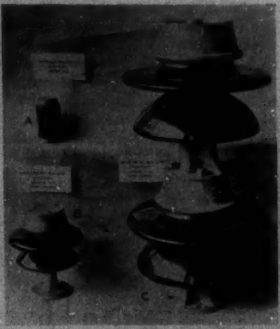

Figure 2. Typical switch and bus insulators tested

A. Apparatus insulator, 15-kv class, indoor type

B. Switch and bus insulator, 15-kv class, outdoor type, National Electrical Manufacturers' Association standard

C. Switch and bus insulator, 69-kv class, outdoor type, NEMA standard

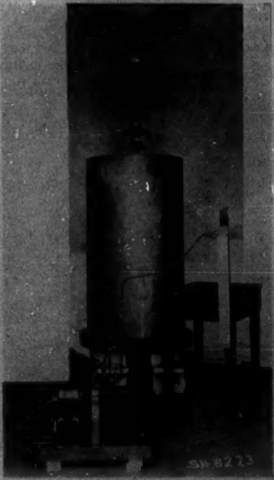

Figure 3. Test arrangement for suspension insulators

Insulator string being lowered into cylinder for test

The technique of testing and measurement fully complies with the recognized methods described in AIEE Standard 4 (1943). In this respect the Sharon high-voltage laboratories are well equipped for both 60-cycle and impulse testing. For instance, an impulse generator of 0.016 microfarad at 3,000 kv and correspondingly higher capacitance for lower test voltages was available. All impulse measurements were recorded with the cathode-ray oscillograph. Typical oscillograms are shown in Figure 15.

Full-Wave Impulse Tests

The critical flashover data for 69-kv and 15-kv switch and bus insulators are plotted in Figure 6. Each point represents the crest of a $1^1/_2$x40-microsecond voltage impulse which caused flashover on 50 per cent of the applications. From normal to 0.47 *RAD*, the lower limit of these tests, each set of points practically determines a straight line. The voltage over this range is directly proportional to the *RAD* plus a constant. One curve extends to 1.23 *RAD* which at 760-millimeter barometric pressure would correspond to −51 degrees centigrade. These curves are typical of the tests made on other outdoor station apparatus and on line insulators.

One point of interest is the longer time to flashover observed at low air density.

Figure 4. Test arrangement for bushing

Table II. Atmospheric Conditions at Various Altitudes

Elevation		Standard Atmosphere*			Isothermal** Atmosphere	Conditions on Earth's Surface	
Feet	Meters	Barometric Pressure (Millimeters)	Temperature (Degrees Centigrade)	$RAD = \frac{0.392b}{273 + t}$	Temperature = 0C Barometric Pressure (Millimeters)	Temperature† (Degrees Centigrade)	Humidity Vapor Pressure (Millimeters)‡
0	0	760	15	1.03	760	20	15.45
3,281	1,000	674	9	0.94	670	10	10.60
6,562	2,000	596	2	0.85	592	7	7.40
9,843	3,000	526	−5	0.77	522	2	5.10
13,120	4,000	462	−11	0.69	461	−3	3.50
19,690	6,000	354	−24	0.56	359	−14	1.70
26,250	8,000	267	−37	0.44	280		
32,800	10,000	198	−50	0.35	218		
39,370	12,000	145	−55	0.26	169		
49,210	15,000	90.7	−55	0.16	116		
65,620	20,000	41.4	−55	0.075	60		

* National Advisory Committee for Aeronautics reports 218 and 538.

** Smithsonian Meteorological Tables, numbers 56 and 57.

† Reference 20, "Mean Temperature for the Entire United States."

‡ References 17 and 19, typical data for standard reference (15.45 millimeters, vapor pressure) at the earth's surface.

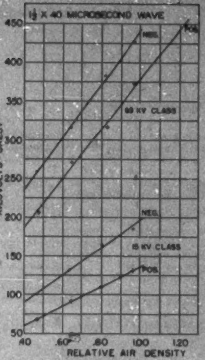

Figure 6. Critical-impulse flashover of standard switch and bus insulators

Atmospheric conditions
Temperature, 25 degrees centigrade
Barometer, 740 millimeters
Humidity, 16 millimeters

In Figure 15 are typical oscillograms. Thus, for the 69-kv bus and switch insulator and a negative wave, the time to flashover at 0.97 *RAD* is seven microseconds and at 0.47 *RAD*, 27 microseconds. Present concepts of the breakdown mechanism appear to be in accord with this effect.

The tests in Figure 7 were made on a 15-kv dry-type current transformer rated 100/5 amperes. This apparatus depends for its strength on tape or layer insulation in combination with the air. Flashover determines the insulation level. The curve is typical for similar indoor station apparatus. The flashover at 0.50 *RAD* again is somewhat better than one-half the voltage at 1.00 *RAD*.

Figure 8 presents typical data for 24-inch, 12-inch, and 6-inch rod gaps. Some of these tests cover a range from 0.10 to 1.50 *RAD*. The variations are much as would be expected for rod gaps. For the most part the curves are straight lines. Tests at low *RAD* show long times to sparkover as noted for apparatus. Other unusual effects were observed; for instance, below 0.05 *RAD* the collapse of the voltage would be abrupt on some tests and in others slow.

Wood is used extensively in transmission and distribution and, lately, in substations. For this reason, it was decided tests should be made. New, seasoned-fir crossarms for a 69-kv line were cut in lengths suitable to enclose in the cylinder and four-foot sections tested. The data, given in Figure 9, substantiate further the fact that wood is quite variable on impulse. However, the curves show clearly that the impulse strength of wood suffers a reduction with *RAD*.

The peculiar behavior of wood is evidenced from these tests. Time to flashover or breakdown for wood is 2 to 3 microseconds at normal *RAD*. At 0.50 *RAD* critical flashover occurs at 12 to 15 microseconds, and this would indicate that air is a primary factor in the breakdown. When breakdown occurs in the air, the voltage collapses suddenly to zero. In some of the tests the collapse is slow, followed by a residual voltage which indicates a discharge in the wood somewhat in the manner of an arrester. These effects are indicated in oscillo-

Figure 5. Testing of wood crossarm

Cylinder provided with porthole window for viewing test

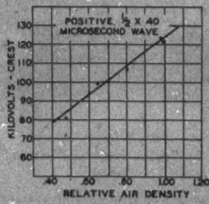

Figure 7. Critical-impulse flashover of 15-kv indoor dry-type current transformer

Atmospheric conditions
Temperature, 27 degrees centigrade
Barometer, 730 millimeters
Humidity, 12.5 millimeters

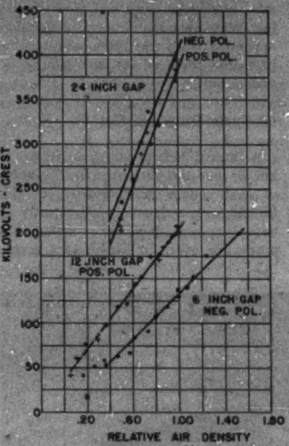

Figure 8. Critical-impulse sparkover of rod gaps

Atmospheric Conditions

Gap	Polarity	Barometer (Millimeters)	Temperature (Degrees Centigrade)	Humidity (Millimeters)
24	negative	730	25	17
24	positive	734	25	19
12	positive	747	20	8
6	negative	733	18	12

grams E of Figure 15. The streamers at the low *RAD*, often extending over a long section of the wood without completing the breakdown, were plainly visible. The splintering was moderate as the currents were limited to a few thousand amperes.

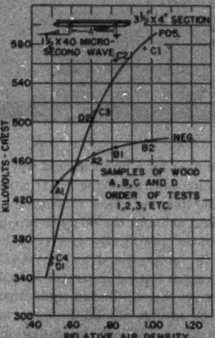

Figure 9. Critical-impulse flashover of four-foot fir crossarm

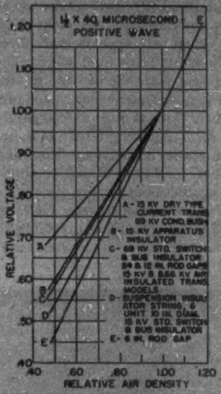

Figure 10. Effect of air density on critical-impulse flashover and sparkover

At the high *RAD* all tests were accompanied by vigorous noise upon flashover or sparkover. At 0.50 *RAD* the noise would dwindle materially and practically vanish at very low densities.

Beside testing typical apparatus, tests were made on insulation of interest in engineering. In Figures 10 and 11, these and the previous data are plotted in the convenient form of correction factors. For the apparatus in air the curves cluster close to a line that runs from unity through zero. A conservative procedure would be to consider the impulse level of station and line apparatus in air as directly proportional to the *RAD* after it is recognized that the positive impulse generally determines the level. For a most exact evaluation of the correction factors reference should be made to Figures 10 and 11.

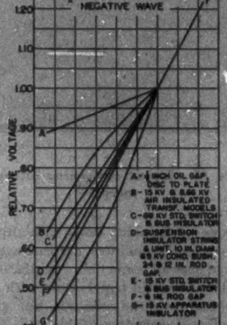

Figure 11. Effect of air density on critical-impulse flashover and sparkover

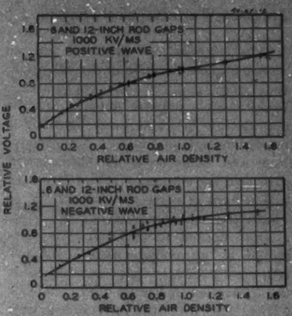

Figure 12. Front-of-wave sparkover of rod gaps

Points are typical of the spread and represen values for 12-inch rod gap

Atmospheric conditions

Temperature, 22 degrees centigrade

Barometer, 742 millimeters

Humidity, 9.0 millimeters

Curve *A* of Figure 11 shows a reduction of ten per cent in the strength of oil from 1.00 to 0.50 *RAD*. This is not a major reduction considering that the corresponding altitude is from sea level to practically 20,000 feet. Solid insulation impregnated in oil is probably affected even less. For this reason, station and line apparatus immersed in insulating liquid can be considered as little affected by altitude. At the very high altitudes the strength of solid and liquid insulation may have to be examined, particularly, if for any reason the low air density affects the condition of the insulation.

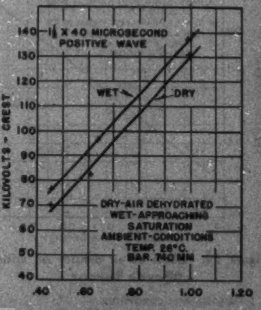

Figure 13. Effect of humidity on critical-impulse sparkover of six-inch rod gap

Front-of-Wave Impulse Tests

Direct strokes of lightning are an important consideration in the co-ordination and protection of apparatus.[23] Front-of-wave impulse data at low air densities are desirable, since lightning is present at high altitudes.[24] Accordingly, tests were made on 6- and 12-inch rod gaps, and the data are given in Figure 12 in the form of correction factors. F of Figure 15 is a typical oscillogram.

From normal to 0.50 *RAD* the correction on an average basis is 0.5 per cent per 0.01 *RAD;* that is, at 0.75 *RAD* the correction factor is 0.87. Within 0.90 to 1.10 *RAD* the correction is less, 0.35 per cent per 0.01 *RAD*. Within this limited *RAD* range a similar correction for short impulses has previously been reported.[25] For practical purposes the curves in Figure 12 can be considered applicable to high-voltage apparatus which have essentially a rod-gap type of characteristic.

Humidity and Other Effects

Comparative tests were made on the six-inch rod gap to determine the effect of humidity at low air densities. First, the tests were made in dry air, supplied from a calcium-chloride–phosphorous-pentoxide desiccator. The tests were repeated in a humid atmosphere produced by a pan of water in combination with cloth wicks introduced into the test cylinder. All tests were with a $1\frac{1}{2}$x40-microsecond positive wave. The results are plotted in Figure 13.

The same vapor pressure maintains for normal and low air pressures.[24] We estimate the humidified air attained 0.80-inch mercury vapor pressure or better. From the two curves we note a constant difference of eight kilovolts throughout. It appears that physically the humidity has a fixed effect. Thus on a per cent basis the increase in strength from dry air to 0.80-inch vapor pressure is nearly twice as great at 0.50 *RAD* as at 1.00 *RAD*. On the strength of these data the humidity corrections determined at sea-level conditions cannot be applied at high altitudes but require a scaling up in an inverse relation to the *RAD*. Additional tests are desirable to establish these relations fully.

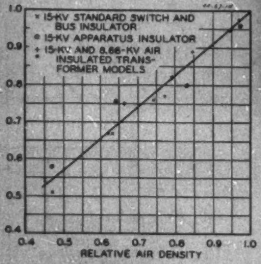

Figure 14. Effect of air density on critical 60-cycle flashover

Atmospheric conditions
Temperature, 29 degrees centigrade
Barometer, 735 millimeters
Humidity, 18.3 millimeters

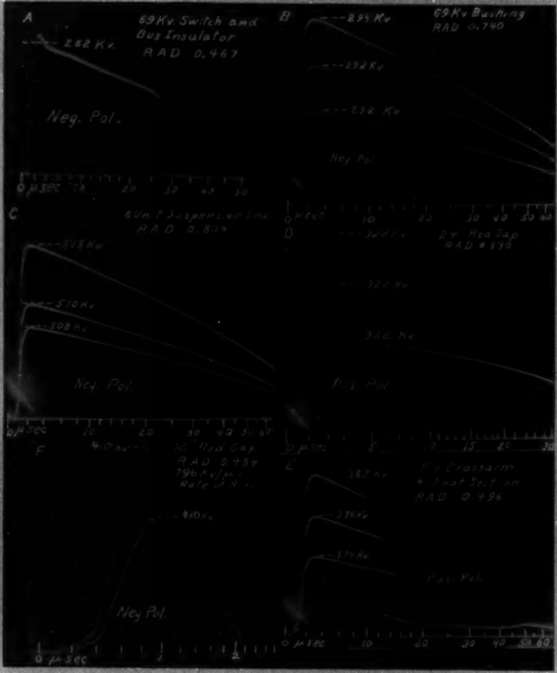

Figure 15. Typical oscillograms of impulse voltages

However, humidity drops with altitude so that actual conditions mitigate against a large over-all correction. On the other hand, the drop in temperature with altitude improves the strength, but again the effect is secondary (Table II). Aside from rain, snow, and so forth, there may be other factors; for instance, little apparently is known of the effect of cosmic rays, ultraviolet rays, and similar ionizing agents upon the strength of electrical apparatus. At the high altitudes in particular these effects may require examination.

60-Cycle Data and Corona

As stated before, 60-cycle studies at low air densities date from the beginning of high-voltage transmission. In this investigation tests were made mostly on apparatus typical of indoor type. The results are given in Figure 14. Breakdown data for a uniform field where temperature controlled the *RAD* from 1.00 to 0.50 likewise follow this curve. From all data at hand the 60-cycle strength of apparatus practically can be taken as directly proportional to the *RAD*. In applying or testing apparatus at high altitudes it seems fundamental that the voltage ratings should conform to this relationship. Where the insulation margins are inherently large and there are no other limitations, as is the case for some of the low-voltage equipment, no derating appears necessary other than possibly at the very high altitudes.

We had planned to investigate corona at low air densities, particularly its effect on insulation and radio noise, but other activities have deferred these tests. For instance, there is the possibility that apparatus may operate at high altitude with more apparent corona than would be permissible at sea level. The dry atmosphere at high altitude may also contribute beneficially to longer insulation life. The question of corona losses on lines is of particular interest to transmission engineers, and this also may require further examining in the light of the actual conditions prevailing at the high altitudes. These are some of the corona effects on which more study and testing may be well worth while.

Summary

The air strength of electrical apparatus to long waves (1½x40 microseconds) is essentially proportional to the *RAD*. The impulse strength of apparatus applied at high altitudes and exposed to lightning should be rated accordingly.

The *RAD* corrections for front-of-wave impulses (1,000 kv/microsecond) are one half and less than one half of those for long waves.

Sixty-cycle strength and corona are also affected in direct relation to the *RAD*. Voltage ratings naturally should recognize this.

The oil strength of apparatus is practically unaffected by altitude.

Data on the effect of humidity are presented. Other influencing factors that may be present at high altitude are discussed. Corona effects may need more attention.

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A 500,000-Kva 7.5-Kv Air Circuit Breaker for Steel-Mill Service

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Synopsis: A self-contained air circuit breaker for a rating of 500,000 kva at 7.5 kv has long been regarded as a desirable goal. Recent developments in magnetic-type air circuit breakers have indicated the economic feasibility of similar designs for ratings higher than previously attained. The development of a 500,000-kva 7,500-volt magnetic air circuit breaker is described. A description of the breaker and the peculiar design problems are presented. Test data with oscillograms are shown.

WITH the advent of the first 15-kv power air circuit breakers for central station service in 1929,[1,2] it was soon recognized that an air circuit breaker was much more desirable for highly repetitive steel-mill motor switching than was the conventional equipment universally used at that time. Interrupting tests showed that the air circuit breaker could withstand many operations on considerable power without requiring maintenance, as compared to the high maintenance necessary on oil switching equipment of that period. This was somewhat to be expected in view of the fact that low-voltage air circuit breakers and contactors had demonstrated this ability for years. It was also believed that the air circuit breaker was highly desirable for steel-mill-substation service because of the additional severity of steel-mill operating conditions, as compared to the duty on corresponding equipment in central-station service.

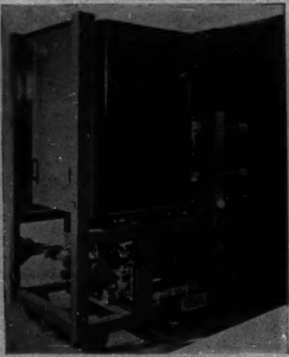

Figure 1. 7.5-kv 250,000-kva deion air circuit breaker for metal-clad switching equipment as widely applied in steel-mill service

Figure 2. Five-kilovolt 150,000-kva magnetic deion air circuit breaker

Realizing these facts, a 250,000-kva 7.5-kv deion air circuit breaker of the metal-plate type was made available in 1932, designed to meet the rigorous demands of steel-mill switching service, for either substation or motor switching duty. Illustrated in Figure 1, this breaker has been widely and successfully applied in some of the largest steel mills in the industry and continues to be applied in substantially its original form. The first breakers of this type, installed on large reversing mill motors in 1932, are still doing duty at the rate of about 50,000 operations per year.

The success of these breakers and of the companion designs for 2.5-, 5-, and 15-kv ratings has been largely responsible for a general trend toward air circuit breakers in this range. However, the problem of interrupting higher currents at higher kilovolt-ampere ratings disclosed economic limitations in the metal plate deion principle indicating limits in current interrupting rating beyond which construction would be too expensive for general use. Thus, the ratings of 250,000 kva at five kilovolts and 500,000 kva at 7.5 kv, involving currents of 30,000 to 45,000 amperes, were considered beyond the range of economic use of the metal plate deion principle. The development of a new principle of arc interruption in air[3,4] made possible the attainment of higher short-circuit current interrupting ability at 2,500 volts than was practical with previous designs. This new principle made use of magnetic diffusion effects with plates of refractory insulating materials.

Through considerable experimental work and study of this principle, five-

Figure 3. Five-kilovolt 250,000-kva magnetic deion air circuit breaker

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kilovolt breakers of 150,000-kva and 250,000-kva interrupting ratings were developed[3] as illustrated in Figures 2 and 3. The short-circuit current which must be interrupted by the 250,000-kva five-kilovolt breaker is 35,000 amperes at the commonly encountered distribution voltage of 4,150. Figure 4 is an oscillogram showing the interrupting by this breaker of 40,000 amperes at 4,020 volts restored, on a single pole, which shows considerable margin for a three-phase rating of 250,000 kva in the five-kilovolt class. This development contributed greatly to the technique of interrupting high currents at higher voltages than previously deemed feasible for a self-contained air circuit breaker.

However, a 500,000-kva breaker for 7.5 kv still remained to complete the line of self-contained air circuit breakers. The breaker herein described completes the need previously indicated.

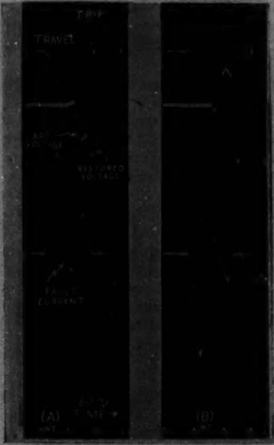

Figure 4. Oscillogram showing interruption of 40,000 amperes at 4,020 volts restored on a single pole of a five-kilovolt 250,000-kva magnetic deion air circuit breaker

Equivalent three-phase kilovolt-amperes—330,000 kva

The general requirements which should be set by a breaker of this rating are:

1. Interrupting capacity of 44,000 amperes at 6,600 volts, three phase.

2. Full interrupting rating at 25 cycles as well as 60 cycles which is often required.

3. General ruggedness and freedom from excessive maintenance requirements.

4. Insulation level adequate for the new insulation standards of 95-kv impulse strength and 36-kv one-minute 60-cycle test.

5. Readily suited to use in the compacted space of metal-clad switching equipment.

The problem of interrupting heavy-current arcs in a restricted space in air is more difficult at the higher currents and especially at the higher voltages. Twenty thousand amperes does not now present a very serious problem, but 40,000 amperes presents much more than double the 20,000-ampere job. The interrupting of 44,000 amperes at 7.5 kv presents problems in arc control and space deionization far beyond those dealing with more moderate currents.

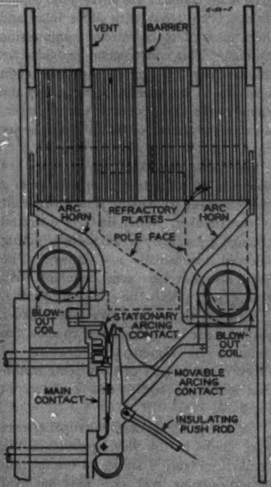

Figure 5. Schematic illustration of construction of 500,000-kva 7.5-kv magnetic deion air breaker

The initial drawing of the arc on the arc tips, even with the best materials available, gives rise to large quantities of ionized gas which must be disposed of if the space between the contacts and other live parts in the arc chamber is to withstand the restored voltage of the circuit. From its inception the arc space must be conditioned progressively to withstand the increasing voltages imposed by the action of the interrupter on the arc. If the arc in moving from the contacts into the interrupter is envisioned in slow motion—keeping in mind the intense heat and ionization—it can be seen that at each point, the space which the arc has just vacated must be sufficiently deionized to withstand the voltage of the arc in the succeeding position. Otherwise, the arc will restrike to the earlier position, from which it must again be moved. Although it is difficult to avoid this restriking with perfection, excessive restriking causes proportionally greater ionization which must be disposed of for successful interruption of the circuit. Finally, the entire space between the live parts of the arc chamber, both internal and external, must withstand the restored voltage applied by the open circuit.

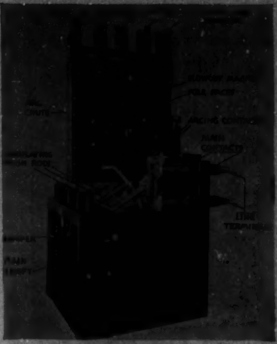

Figure 6. Three-pole 500,000-kva 7.5-kv magnetic deion air breaker showing one arc chute in position

The interrupting element or arc chute of this breaker is arranged so that the arc is moved by a magnetic field into V-shaped slots in a multiplicity of refractory plates separated from each other. The planes of the plates are perpendicular to the arc stream. The arc chute is shown schematically in Figure 5. The lower part encloses the initial arc-drawing space and the space for extending the arc length for movement into the series of refractory deionizing plates.

A curved arc horn is located at each end of this lower part of the arc chute to provide rapidly separating arc terminal paths. Each arc horn is on the immediate upper periphery of a circular blowout coil. The coil above the stationary arcing contact is cut into series relationship with the arc as the arc impinges on the arc horn immediately above that coil. This adds sufficient magnetic field to that existing from the loop shape of the current path through the breaker to move the terminal of the arc on the movable arc tip rapidly onto the arc horn at the other end of the arc

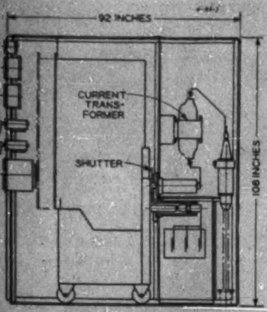

Figure 7. Dimensioned drawing showing 500,000-kva 7.5-kv magnetic deion air breaker in metal-clad switching equipment

Type 75–DH–500 drawout unit 40 inches wide

chute and the corresponding blowout coil is introduced into the circuit. The magnetic field provided by the two blowout coils is applied to the arc space by suitably shaped pole faces, to move the arc as uniformly and as rapidly as possible consistent with the deionization of all of the space behind it. The deionization of this space between the contacts and arc horns is accomplished by venting the gases from the arc and cooling them to such an extent that breakdown will not occur. To this end the arc horns are vented freely, and the refractory plates above the arc horns are arranged in four groups spaced apart to provide five relatively wide vents evenly spaced.

The refractory plates are separated as much as possible for venting effect without sacrificing the deionization effect on the exhaust gases traveling from the ends of the V-shape slots to the ends of the plates. Lateral barriers are also placed at the end stack vent as an additional precaution against flashing over the entire end of the arc chamber.

In Figure 5, the arcing contacts are externally similar to those previously applied to magnetic deion breakers for 100,000, 150,000, and 250,000 kva[3–5] but are equipped with heavier springs to prevent blowing open on the high currents involved. The arc tips and secondary contacts are made of silver tungsten. The main contacts are of the solid copper bridge type, faced with silver inlay and held in contact by strong compression springs. This construction provides for transferring the current from the main contacts to the arcing contacts, and the shape of the arcing contact parts accelerates the movement of the arc from the arc tips.

The three movable contact arms are operated by insulating push rods from a shaft running lengthwise across the front of the breaker. This shaft is operated through a toggle by a horizontally acting solenoid mechanism not shown. The shaft also has two arms coacting with bumpers to absorb the shock of opening.

Figure 6 shows the three-pole breaker with two arc chutes removed to show the contacts. A dimensional drawing of this breaker as part of a complete metal-clad switching equipment is shown in Figure 7.

Extensive high-power interrupting tests were made in the development of the breaker. Figure 8 shows an oscillogram of an opening test on the completed breaker of 47,500 amperes at 5,900 volts, 60 cycles on a single pole. Figure 9 shows a close–open test of 45,700 amperes at 6,100 volts, 25 cycles on a single pole.

Table I. Single-Phase Interrupting Tests

7,500-Volt 500,000-Kva Steel-Mill Circuit Breaker

Test Number	Generator Voltage	Rms Amperes Interrupted	Arcing Time (Cycles)	Interrupting Time (Cycles)
Opening Tests 60 Cycles				
1	3,500	11,000	1.50	2.75
2	5,900	11,900	1.20	2.60
3	5,950	21,400	1.00	2.40
4	5,900	34,800	1.00	2.40
5	3,500	11,400	1.00	2.50
6	5,900	30,500	0.80	2.30
7	5,900	29,200	0.90	2.30
8	5,900	21,000	0.80	2.20
9	5,900	46,550	0.80	2.20
10	5,900	47,250	1.10	2.60
11	5,900	47,250	0.75	2.30
12	5,900	46,950	1.25	2.60
Opening Tests 25 Cycles				
13	5,850	5,225	0.50	1.00
14	5,850	21,850	0.45	0.90
15	3,500	20,250	0.45	0.95
16	6,100	29,200	0.40	1.20
17	6,100	43,700	0.50	1.00
18	6,150	42,800	0.40	0.90
Close–Open Tests 25 Cycles				
19	6,100	1,415	1.20	1.70
20	6,100	42,900	0.40	0.90
21	6,100	45,700	0.60	1.20

Table I shows three series of tests on a single pole of the breaker, all of which were made without maintenance. Of the 21 tests, 8 are at more than 40,000 amperes and 16 are at more than 20,000 amperes. This performance is equivalent to years of actual service. Proof of satisfactory performance over the entire range of lower currents was obtained by subjecting the breaker, without any form of maintenance, to a series of approximately 100 interrupting tests.

Conclusion

The principle of magnetic diffusion has been applied to a complete line of circuit breakers with interrupting capacities

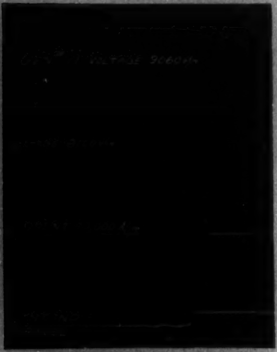

Figure 8. Oscillogram showing the interruption of 47,500 amperes at 5,900 volts at 60 cycles on a single pole of the 500,000-kva 7.5-kv magnetic deion air breaker

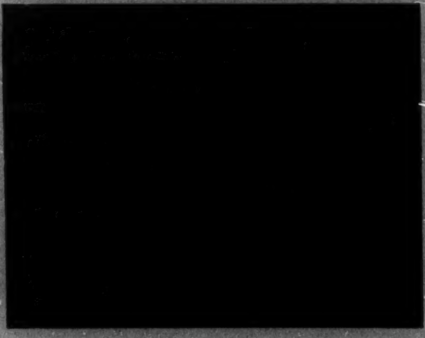

Figure 9. Oscillogram similar to Figure 9 except showing a single-phase close–open interruption of 45,700 amperes with a generator voltage of 6,100 and at 25 cycles

ranging from 50,000 kva[6] at 2,500 volts to the 500,000-kva 7.5-kv breaker described in this paper. Laboratory tests show that this most recent breaker is well suited to the operating conditions of steel mill and equivalent service and that it has ample short-circuit interrupting capacity for use in large power concentrations at 6,600 or 6,900 volts.

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The Asymmetrical Stator as a Means of Starting Single-Phase Induction Motors

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THE shaded-pole motor is quite simple to construct; but for all but the smallest sizes, losses are large; and if distributed windings are used, its construction becomes more complicated. A single-phase induction motor may be given starting torque by varying the rotor constants over the pole face, either by asymmetrical iron structure or winding. Starting torque is comparable with that of a shaded-pole motor, and construction is very simple with nothing to get out of order. The chief drawback appears to be the smaller power factor which is due to reduced mutual inductance.

Means of Securing an Asymmetrical Stator

Starting torque arises from two sources:

1. The difference in leakage inductance of the rotor bars on one side of the pole from that on the other causes a different current to flow on each side, producing a resultant torque, as in a repulsion motor.

2. The difference in mutual reactance on the two sides of the poles gives the rotor current under the two sides some difference in phase, which also produces a resultant torque.

One means of securing these differences is to wind more turns about one side of the pole face, which we will designate as *a*, than about the other side, designated *b*, thus causing a difference in the permeability of both the rotor and stator iron. If the iron at *b* has the maximum permeability, the flux density at *a* will be above that of maximum permeability; the rotor bars under *b* will have greater inductance than those under *a*, and since the current through these encircles *a*, the component of rotor leakage reactance X_{2a} will be greater than X_{2b}. For a lower voltage and flux density, the opposite will be the case; hence, we have the interesting possibility of the starting direction being reversed by a change in starting voltage. In order to utilize this effect fully, the rotor bars should be surrounded completely by iron. It is quite difficult to calculate the rotor reactance of this type of motor; thus, it is hard to design efficiently.

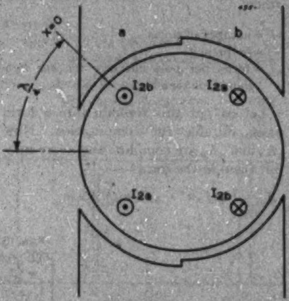

Figure 1. Relative position of fluxes and currents

The case of varying the air gap over the pole face is easier to calculate. These formulas are derived in the appendix. While the greatest difference in both rotor inductance and mutual reactance occurs when the air gap has its maximum value over half the pole face, changing abruptly to a minimum, harmonic reactances of the rotor are least when the so-called zigzag reactance, that is, leakage reactance of the rotor bars due to flux crossing the air gap, varies sinusoidally over each pole. If it is assumed that this reactance is to be inversely proportional to the air gap, this requires that

$$1/\Delta = {}_0A + {}_1A \cos 2\pi x/\lambda \qquad (1)$$

where

Δ = air gap (all dimensions in this paper are given in inches)
${}_0A = (1/2)(1/\Delta_{min} + 1/\Delta_{max})$
${}_1A = (1/2)(1/\Delta_{min} - 1/\Delta_{max})$
x = co-ordinate measured along rotor periphery
λ = pole pitch

It might seem desirable to create a sinusoidal flux wave by increasing the turns about each stator tooth in proportion to the increase in air gap under that tooth. This introduces harmonics into the magnetomotive-force wave which gives a greater harmonic content to the mutual reactance than an ordinary sinusoidally distributed winding would give.

Difference in flux density over *a* and *b* may cause difference in rotor permeability, which may increase or decrease the resultant torque; but unless the rotor bars are surrounded completely by iron, this effect should be small. Difference in flux density over *a* and *b* also may produce a difference in hysteretic angle of advance between flux *a* and *b*, producing a rotating field; but this effect is also quite small under ordinary conditions.

Computation of Rotor Currents and Torque

P. H. Trickey[1] has given an analysis of the shaded-pole motor which forms the basis of this development. The space distribution waves of field flux and rotor current are resolved into Fourier series. Each harmonic is divided into two components or phases 90 degrees apart, denoted by subscripts *a* and *b*. In Appendix II the values of these components are calculated. Figure 1 shows the relative position of the fields and currents for the

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fundamental flux wave; Figure 2 shows the equivalent circuit diagram. If we add voltages about the shunt circuits a and b associated with the nth harmonic, we get

$$({}_nR_{2a}+j\,{}_nX_{2a}){}_nI_{2a}=(I-{}_nI_{2a})j\,{}_nX_a+nS(I-{}_nI_{2b}){}_nX_b-nS\,{}_nI_{2b}\,{}_nX_{2b} \quad (2)$$

$$({}_nR_{2b}+j\,{}_nX_{2b}){}_nI_{2b}=(I-{}_nI_{2b})j\,{}_nX_b-nS(I-{}_nI_{2a}){}_nX_a+nS\,{}_nI_{2a}\,{}_nX_{2a} \quad (3)$$

where

${}_nR_{2a}$ = rotor resistance to nth space harmonic of current, phase a

(All impedances mentioned in body of paper are referred to primary.)

${}_nX_{2a}$ = rotor leakage reactance, nth harmonic, phase a
${}_nX_a$ = mutual reactance, nth harmonic, phase a.
${}_nI_{2a}$ = rotor current, nth harmonic, phase a

(Similar quantities of phase b have subscript b.)

n = order of harmonic
S = ratio of rotor speed to synchronous speed, positive in direction of increasing x

Ordinarily the fundamental flux, $n=1$, is all that need be considered; but with a salient pole the third harmonic is apt to affect the torque appreciably. The equivalent resistance, R_h, due to iron loss, should be included as shown, but its computation is outside the province of this paper, its chief effect being to increase the inphase component of current.

If equations 2 and 3 are solved simultaneously for ${}_nI_{2a}$ and ${}_nI_{2b}$

$${}_nI_{2a}={}_nK_aI \qquad {}_nI_{2b}={}_nK_bI \quad (4)$$

$${}_nK_a={}_nF_a/{}_nF \qquad {}_nK_b={}_nF_b/{}_nF \quad (5)$$

$${}_nF_a=nS\,{}_nR_{2b}\,{}_nX_b-(1-n^2S^2){}_nX_{ob}\,{}_nX_a+j\,{}_nR_{2b}\,{}_nX_a \quad (6)$$

$${}_nF_b=-nS\,{}_nR_{2a}\,{}_nX_a-(1-n^2S^2){}_nX_{oa}\,{}_nX_b+j\,{}_nR_{2a}\,{}_nX_b \quad (7)$$

$${}_nF={}_nR_{2a}\,{}_nR_{2b}-(1-n^2S^2){}_nX_{oa}\,{}_nX_{ob}+j({}_nR_{2a}\,{}_nX_{ob}+{}_nR_{2b}\,{}_nX_{oa}) \quad (8)$$

where

${}_nX_{oa}={}_nX_a+{}_nX_{2a}$ and so forth.

In the primary circuit

$$I(R_1+jX_1)+(I-{}_1I_{2a})j\,{}_1X_a+(I-{}_1I_{2b})j\,{}_1X_b+(I-{}_3I_{2a})j\,{}_3X_a+(I-{}_3I_{2b})j\,{}_3X_b=E$$

where

R_1 = primary resistance
X_1 = total primary leakage reactance, excluding ${}_3X_a$ and ${}_3X_b$
E = voltage

If the values of ${}_1I_{2a}$ and so forth are put in from equation 4

$$I[R_1+j(X_t+X_1')-{}_1K_a\,j\,{}_1X_a-{}_1K_b\,j\,{}_1X_b-{}_3K_a\,j\,{}_3X_a-{}_3K_b\,j\,{}_3X_b]=E \quad (9)$$

where

X_t = total mutual reactance
X_1' = primary leakage reactance, excluding harmonic and skew reactance

Torque in ounce inches for the nth harmonic is given by the real part of

$${}_nT=11.3(nP/f)[({}_nt_b/{}_nt_a){}_nX_a(I-{}_nI_{2a})\times \text{conj}\,{}_nI_{2b}-({}_nt_a/{}_nt_b){}_nX_b(I-{}_nI_{2b})\times \text{conj}\,{}_nI_{2a}] \quad (10)$$

where

P = number of poles
f = frequency
${}_nt_a$ = turns per pole effective for nth harmonic, phase a

Let us put this formula into a form more suitable for computation. Now ${}_nK_a$ and ${}_nK_b$ are complex, and so we may put them in the form

$${}_nK_a={}_nU+j\,{}_nu \qquad {}_nK_b={}_nV+j\,{}_nv$$

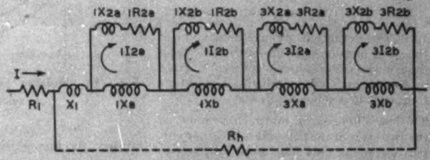

Figure 2. Equivalent circuit diagram of asymmetrical field induction motor

If we substitute these expressions in equation 4, we find that the real part of

$$(I-{}_nI_{2a})\ \text{conj}\ {}_nI_{2b}=I^2(1-{}_nU-j\,{}_nu)({}_nV-j\,{}_nv)=I^2[{}_nV-({}_nU\,{}_nV+{}_nu\,{}_nv)]$$

$$(I-{}_nI_{2b})\ \text{conj}\ {}_nI_{2a}=I^2(1-{}_nV-j\,{}_nv)\times({}_nU-j\,{}_nu)=I^2[{}_nU-({}_nU\,{}_nV+{}_nu\,{}_nv)]$$

so that

$${}_nT=11.3(nPI^2/f)[({}_nt_b/{}_nt_a){}_nX_a[{}_nV-({}_nU_nV+{}_nu_nv)]-({}_nt_a/{}_nt_b){}_nX_b[{}_nU-({}_nU_nV+{}_nu_nv)]] \quad (11)$$

Standstill Conditions

When $S=0$, the system of equations 4 to 8 reduces to

$${}_nK_a=j\,{}_nX_a/({}_nR_{2a}+j\,{}_nX_{oa})=({}_nK_{ra}/{}_nX_a)({}_nX_{oa}+j\,{}_nR_{2a}) \quad (12)$$

where

$${}_nK_{ra}=({}_nX_a/{}_nX_{oa})^2/[1+({}_nR_{2a}/{}_nX_{oa})^2]$$

with a similar formula for b.

When $t_a=t_b$ and $R_{2a}=R_{2b}=R_2/2$ (R_2 being the complete fundamental rotor resistance as calculated by usual methods), as is often the case, equation 12 becomes, for $n=1$

$${}_1T=11.3(P\,{}_1K_{ra}\,{}_1K_{rb}/{}_1X_a\,{}_1X_bf)I^2\times[({}_1X_{2b}-{}_1X_{2a})({}_1R_2^2-{}_1X_{oa}\,{}_1X_{ob})-2\,{}_1R_2^2({}_1X_a-{}_1X_b)] \quad (13)$$

showing that the torque depends on both ${}_1X_{2b}-{}_1X_{2a}$ and ${}_1X_a-{}_1X_b$. Since we may put ${}_1R_{2a}=R_2'\,{}_1t_a^2$ and ${}_1X_{2a}=X_{2a}'\,{}_1t_a^2$, and so forth, it becomes evident on putting these values in equation 11 for $S=0$, that starting torque is independent of any difference in t_a and t_b, except as they affect the permeability.

Motor Constants With Sinusoidal Permeance

With an ordinary symmetrical winding, $t_a=t_b=t_p/\sqrt{2}$ and $R_{2a}=R_{2b}=R_2/2$, where t_p is the effective turns per pole calculated in the usual manner.

With the air gap of equation 1, and with any type of winding

$$R_{2a}=8(Pt_a)^2\left[\left(\sqrt{W^2+Sk^2}/S_sS_c\right)+(0.637D_rK_r/S_rP^2)\right]\rho \quad (14)$$

where we have dropped the subscript for harmonics as we are going to deal with the fundamental only, and

S_s = number of rotor slots
S_c = cross section of rotor bar
Sk = skew in inches
S_r = cross section of end ring
W = axial width of rotor
ρ = resistivity
D_r = diameter of end ring
K_r = ring constant (see reference 2)

For R_{2b} use t_b instead of t_a

$$X_{2a}=X_{sa}+X_{ska}+(X_{sa\,\max}+X_{sa\,\min})/2-(X_{sa\,\max}-X_{sa\,\min})/4 \quad (15)$$

$$X_{2b}=X_{sb}+X_{skb}+(X_{sb\,\max}+X_{sb\,\min})/2+(X_{sb\,\max}-X_{sb\,\min})/4 \quad (16)$$

X_{sa} = rotor slot reactance, phase a
$=2\pi f(Pt_a)^2\,10^{-8}25.52\,(WK_s/S_s) \quad (17)$

K_s = rotor slot constant (see reference 1)

For X_{sb} use t_b

$$X_{sa\,\max}=2\pi f(Pt_a)^2 10^{-8}25.52(WT_f/S_s4\Delta_{\min}) \quad (18)$$

T_f = rotor tooth face in inches

For $X_{ss\ \min}$ use $\Delta_{\max}$, for $X_{sb\ \min}$ use t_b and $\Delta_{\max}$, and so forth. The preceding formula applies only when there are no stator slots to break the magnetic circuit, and when the rotor bars are not completely surrounded by iron

$$X_{sta}=(1-C_{sk})X_a'$$
$$X_{stb}=(1-C_{sk})X_b' \qquad (19)$$

$$C_{sk}=\sin(\pi Sk/2\lambda)/(\pi Sk/2\lambda) \qquad (20)$$

Since $\sin x=x-x^3/6$ for small values of x

$$1-C_{sk}=(1/6)(\pi Sk/2\lambda)^2 \qquad (21)$$

approximately for small values of Sk/λ.

Thus

$$X_{2b}-X_{2a}=(X_{z\ \max}-X_{z\ \min})/2-(X_a-X_b)(1-C_{sk}) \qquad (22)$$

If equation 22 is substituted in equation 13, the term in brackets becomes

$$(R_2^2-X_{oa}\,X_{ob})(X_{z\ \max}-X_{z\ \min})/2-(X_a-X_b)[(R_2^2-X_{oa}\,X_{ob})(1-C_{sk})+2R_2^2] \qquad (23)$$

Thus the effect of zigzag reactance is opposed to that of skew. When R_2 is small, it may be desirable to reduce X_{2a} and X_{2b} as much as possible, depending on $X_{sta}-X_{stb}$ for starting torque by increasing the skew an integral number of slots. When $2R_2$ is greater than $(R_2^2-X_{oa}X_{ob})(1-C_{sk})$, it may be desirable to reduce the skew as much as possible. Since, in all practical cases, R_2^2 is less than $X_{oa}X_{ob}$, torque from zigzag reactance in this case is in the same direction as that due X_a-X_b. The increase of R_2 increases the torque per ampere (until it approaches the magnitude of X_{ob}), though it reduces the current; however, increase of R_2 increases the slip and losses and decreases the power factor, as in any induction motor. Since X_a-X_b and $X_{2b}-X_{2a}$ usually must be increased by reducing X_b, the starting torque is limited by the decrease in power factor and increase in rotor losses permissible.

Appendix I. Equivalent Impedance of Squirrel-Cage Rotor

Let there be an infinite number of rotor bars having the same impedance for an inch of periphery as the actual rotor. Figure 3 represents an elementary circuit about the rotor. AB and DC are the endrings, AD being the bar where the ring current $I_r=0$. Then at x

$$I_r=\int_c^x J\,dx$$

where J = current density in bars per inch of periphery.

The voltage drop E_r in each ring from x to d is

$$E_r=R_r\int_x^d I_r\,dx=R_r\int_x^d dx\int_c^x J\,dx$$

where R_r = resistance per inch of end ring.

Let the voltage induced at x be $E(x)$ and that, at a fixed point, d be $E(d)$. Then the voltage about the whole circuit is

$$J\,dx(Z_1/dx)+2R_r\int_x^d dx\int_c^x xJ\,dx=E(x)-E(d) \qquad (24)$$

where

$Z_1=Z_B\lambda P/S_s$ = impedance of one inch of bars at x

Z_B = actual impedance of one bar (25)

If we differentiate twice, and remember that $E(d)$ is constant

$$(d^2/dx^2)(JZ_1)+2R_rJ=d^2E/dx^2 \qquad (26)$$

We may represent J, E, and Z_1 in a Fourier series in which

$$\theta=\pi x/\lambda$$
$$J={}_1J_a\sin\theta+{}_3J_a\sin 3\theta+\ldots+{}_1J_b\cos\theta+{}_3J_b\cos 3\theta+\ldots \qquad (27)$$

$$E={}_1E_a\sin\theta+{}_3E_a\sin 3\theta+\ldots+{}_1E_b\cos\theta+{}_3E_b\cos 3\theta+\ldots \qquad (28)$$

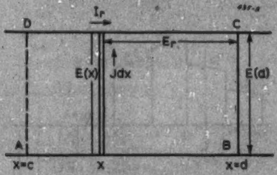

Figure 3. Rotor circuit diagram

$$Z_1={}_0C+{}_2C\sin 2\theta+{}_4C\sin 4\theta+\ldots+{}_0D+{}_2D\cos 2\theta+\ldots \qquad (29)$$

Z_1 has a period of 2θ because it repeats at every pole. In order to solve the differential equation 26, it is necessary only to substitute in it equations 27, 28, and 29. Now, if we put equations 27 and 29 in more concise form

$$JZ_1=\left(\sum_q {}_qJ_a\sin q\theta+\sum_q {}_qJ_b\cos q\theta\right)\times\left(\sum_p {}_pC\sin p\theta+\sum_p {}_pD\cos p\theta\right) \qquad (30)$$

If we use product formulas of the type

$$\sin m\theta\sin n\theta=-(1/2)\cos(m+n)\theta+(1/2)\cos(m-n)\theta$$

we find, by expanding equation 30

$$JZ_1=(1/2)\sum_p\sum_q {}_pC\,{}_qJ_a[-\cos(p+q)\theta+\cos(p-q)\theta]+(1/2)\sum_p\sum_q {}_pC_qJ_b[\sin(p+q)\theta+\sin(p-q)\theta]+(1/2)\sum_p\sum_q {}_pD_qJ_a\times[\sin(p+q)\theta-\sin(p-q)\theta]+(1/2)\sum_p\sum_q {}_pD_qJ_b[\cos(p+q)\theta+\cos(p-q)\theta] \qquad (31)$$

where the summation extends over all the positive values of p and q for which D and C exist. Pick out the coefficient of $\sin n\theta$ in ${}_pC_qJ_b\sin(p+q)\theta$, where $n=p+q$; it is

$$\sum_q {}_{n-q}D\,{}_qJ_b$$

Likewise, in the term ${}_pC_qJ_b\sin(p-q)\theta$, the coefficient of $\sin n\theta$, where $n=p-q$, is

$$\sum_q {}_{n-q}C\,{}_qJ_b-\sum_q {}_{q-n}C\,{}_qJ_b$$

for when $q>p$, n is negative, and the terms are of the form

$${}_{q-|n|}C\,{}_qJ_b\sin(-|n|\theta)=-{}_{q-|n|}C\,{}_qJ_b\sin|n|\theta$$

where $|n|=q-p$. If we pick out the other coefficients in the same way

$$Z_1J=(1/2)\sum_n {}_nM_a\sin n\theta+(1/2)\sum_n {}_nM_b\cos n\theta \qquad (32)$$

where

$${}_nM_a=\sum_q {}_{n-q}C\,{}_qJ_b-\sum_q {}_{q-n}C\,{}_qJ_b+\sum_q {}_{n+q}C\,{}_qJ_b+\sum_q {}_{n-q}D\,{}_qJ_a+\sum_q {}_{q-n}D\,{}_qJ_a-\sum_q {}_{n+q}D\,{}_qJ_a \qquad (33)$$

$${}_nM_b=-\sum_q {}_{n-q}C\,{}_qJ_a+\sum_q {}_{q-n}C\,{}_qJ_a+\sum_q {}_{n+q}C\,{}_qJ_a+\sum_q {}_{n-q}D\,{}_qJ_b+\sum_q {}_{q-n}D\,{}_qJ_b+\sum_q {}_{n+q}D\,{}_qJ_b \qquad (34)$$

As before, the summation extends over all positive values of q for which the factors C and D exist.

We are ready now to substitute the value of JZ_1, that is, equation 32, in equation 26

$$-\sum(n^2\pi^2/2\lambda^2)_nM_a\sin n\pi x/\lambda-\sum(n^2\pi^2/2\lambda^2)_nM_b\cos n\pi x/\lambda-2R_r\sum {}_nJ_a\sin n\pi x/\lambda-2R_r\sum {}_nJ_b\cos n\pi x/\lambda=-(\pi^2/\lambda^2)\left[\sum n^2\,{}_nE_a\sin n\pi x/\lambda-\sum n^2\,{}_nE_b\cos n\pi x/\lambda\right] \qquad (35)$$

We may separate equation 35 in a different equation for each harmonic

$$[(1/2)_nM_a+2\lambda^2R_r\,{}_nJ_a/n^2\pi^2\sin(n\pi x/\lambda)]={}_nE_a\sin(n\pi x/\lambda) \qquad (36)$$

$$[(1/2)_nM_b+2\lambda^2R_r\,{}_nJ_b/n^2\pi^2\cos(n\pi x/\lambda)]={}_nE_b\cos(n\pi x/\lambda) \qquad (37)$$

Let us now confine our attention to the case in which all orders of C are zero. Then each value of ${}_nM_a$ is made up of terms including ${}_mJ_a$, and we get the system of equations in J_a of the form

$$({}_{11}Z_a'\,{}_1J_a+{}_{13}Z_a'\,{}_3J_a+{}_{15}Z'_a\,{}_5J_a+\ldots)\times\sin\pi x/\lambda_1E_a\sin\pi x/\lambda$$

$$({}_{31}Z'_a\,{}_1J_a+{}_{33}Z_a'\,{}_3J_a+{}_{35}Z_{1a}\,{}_5J_a+\ldots)\times\sin 3\pi x/\lambda={}_3E_a\sin 3\pi x/\lambda \qquad (38)$$

$$\left(\sum_m {}_{nm}Z'_a\,{}_mJ_a\right)\sin(n\pi x/\lambda)={}_nE_a\times\sin(n\pi x/\lambda)$$

If we multiply by ${}_nJ_a \sin(n\pi x/\lambda)\,dx$, the element of current at x, gives d_nW, the element of rotor losses$+j$.reactive power for the nth harmonic. Integrating over the whole rotor

$$P\int_0^{\lambda}\sum_m {}_{nm}Z'_a\,{}_mJ_a \sin^2(n\pi x/\lambda)dx={}_nW\,{}_nJ_a \tag{39}$$

$$(P\lambda/2)\,{}_nJ_a\sum_m {}_{nm}Z'_a\,{}_mJ_a={}_nW$$

Now the total current that encircles x of the mth harmonic is

$${}_mJ_a\int_x \sin(m\pi x/\lambda)dx$$

Since this flows through one turn, it is also the magnetomotive force of the secondary

$$MMF_2=(\lambda_m J_a/\pi m)\cos(m\pi x/\lambda) \tag{40}$$

The magnetomotive force of the primary is $(4/\pi)\,{}_mt_a\,{}_mI_{2a}\cos(m\pi x/\lambda)$. Equating primary and secondary magnetomotive force

$${}_mJ_a=(4m/\lambda)\,{}_mt_a\,{}_mI_{2a} \tag{41}$$

Substitution in equation 39 gives

$${}_nW=(8F/\lambda)n\,{}_nt_a\,{}_nI_{2a}\sum_m m\,{}_mt_a\,{}_mI_{2a}\,{}_{nm}Z'_a \tag{42}$$

Thus ${}_nW$ has the form

$${}_{n1}Z_a\,{}_nI_{2a}\,{}_1I_{2a}+{}_{n3}Z_a\,{}_nI_{2a}\,{}_3I_{2a}+{}_{n5}Z_a\,{}_nI_{2a}\,{}_5I_{2a}+\ldots$$

and if corresponding products of I_{2a} are equated to those of J_a

$${}_{nm}Z_a=(8P/\lambda)n\,{}_nt_a\,m\,{}_mt_a\,{}_{nm}Z'_a \tag{43}$$

where the D's that make up ${}_{nm}Z'_a$ are given in terms of one inch of rotor periphery. Since, in equation 25

$$Z_1=Z_B\lambda P/S_s$$

$${}_{nm}Z'_a={}_{nm}Z''_a\lambda P/S_s$$

and

$${}_{nm}Z_a=(8P^2/S_s)n\,{}_nt_a\,m\,{}_mt_a\,{}_{nm}Z''_a \tag{44}$$

where ${}_{nm}Z''_a$ is given in terms of impedance per bar. The formula for phase b is obtained, as usual, by substituting quantities in b in place of those with subscript a. Thus, we have the system of equations giving the rotor voltage in terms of the harmonic currents, all referred to the stator

$${}_{11}Z_a\,{}_1I_{2a}+{}_{13}Z_a\,{}_3I_{2a}+{}_{15}Z_a\,{}_5I_{2a}+\ldots={}_1E_a$$

$${}_{31}Z_a\,{}_1I_{2a}+{}_{33}Z_a\,{}_3I_{2a}+{}_{35}Z_a\,{}_5I_{2a}+\ldots={}_3E_a \tag{45}$$

The impedances of form ${}_{nm}Z$ can be neglected usually, in which case equation 45 becomes the equations we used in the body of the paper, equations 2 and 3.

As the simplest example, let $Z_1={}_0D+{}_2D\cos 2\pi x/\lambda$ which is the impedance given by the air gap represented in equation 1. If we expand ${}_nM_a$ from equation 33 and ${}_nM_b$ from equation 34

$${}_1M_a=(2\,{}_0D-{}_2D)\,{}_1J_a+{}_2D\,{}_3J_a$$
$${}_3M_a={}_2D\,{}_1J_a+2\,{}_0D\,{}_3J_a+{}_2D\,{}_5J_a$$
$${}_5M_a={}_2D\,{}_3J_a+2\,{}_0D\,{}_5J_a+{}_2D\,{}_7J_a$$
$${}_1M_b=(2\,{}_0D+{}_2D)\,{}_1J_b+{}_2D\,{}_3J_b$$
$${}_3M_b={}_2D\,{}_1J_b+2\,{}_0D\,{}_3J_b+{}_2D\,{}_5J_b$$
$${}_5M_b={}_2D\,{}_3J_b+2\,{}_0D\,{}_5J_b+{}_2D\,{}_7J_b$$

If we put these values in equation 38

$$(1/2)(2\,{}_0D-{}_2D+K')\,{}_1J_a+{}_2D\,{}_3J_a={}_1E_a$$
$$(1/2)\,{}_2D\,{}_1J_a+(2\,{}_0D+K'/9)\,{}_3J_a+{}_2D\,{}_5J_a={}_3E_a$$
$$(1/2)\,{}_2D\,{}_3J_a+(2\,{}_0D+K'/25)\,{}_5J_a+{}_2D\,{}_7J_a={}_5E_a \tag{46}$$

There are similar equations for b, except that all signs are positive.

Comparing with equation 38, we see that the coefficient of ${}_mJ_a$ is identical with ${}_{nm}Z$. If we desire to express the D's in terms of impedance per bar, as in equation 44, we shall have to multiply K' by $S_s/P\lambda$.

$$K=(4R_r\lambda^2/\pi^2)(S_s/\lambda P)=4R_r\lambda S_s/\pi^2 P$$
$$=4R_rD_rS_s/P^2\pi \tag{47}$$

where D_r=diameter of ring at ends of conductors. From equations 44 and 46 we find, putting

$${}_{nm}G_a=(4P^2/S_s)n\,{}_nt_a\,m\,{}_mt_a \tag{48}$$

$${}_{11}Z_a={}_{11}G_a(2\,{}_0D-{}_2D+K)$$
$${}_{13}Z_a={}_{13}G_a\,{}_2D$$
$${}_{15}Z_a=0$$
$${}_{31}Z_a={}_{31}G_a\,{}_2D$$
$${}_{33}Z_a={}_{33}G_a(2\,{}_0D+K/9)$$

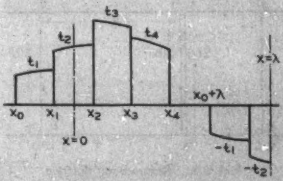

Figure 4. Diagram illustrating notation of stator teeth

$${}_{35}Z_a={}_{35}G_a\,{}_2D$$
$${}_{51}Z_a=0$$
$${}_{53}Z_a={}_{53}G_a\,{}_2D$$
$${}_{55}Z_a={}_{55}G_a(2\,{}_0D+K/25) \tag{49}$$

In a similar manner we find

$${}_{nm}G_b=(4P^2/S_s)n\,{}_nt_b\,m\,{}_mt_b \tag{50}$$

$${}_{11}Z_b={}_{11}G_b(2\,{}_0D+{}_2D+K) \tag{51}$$

All the other Z_b's are obtained from equation 49 by using G_b instead of G_a.

Computing Bar Constants

The resistance per bar is given by

$$R_B=\rho\sqrt{W^2+Sk^2}/S_c \tag{51a}$$

where

ρ=resistivity in ohms per inch cube
W=length of rotor
Sk=skew in inches
S_c=section of conductor

The ring resistance per inch is

$${}_nR_r=\rho\,{}_nK_r/S_r \tag{52}$$

where

S_r=section of ring
${}_nK_r$=Trickey's ring constant[3]
$=(nP/2)(1-ID/D_r)[1+(ID/D_r)^{Pn}]+[1-(ID/D_r)^{Pn}]$

The leakage reactance is divided into three components:

1. SLOT REACTANCE

$$X_s=2\pi f3.19\times10^{-8}WK_s$$

where

$$K_s=\text{rotor slot constant}^1 \tag{53}$$

2. ZIGZAG REACTANCE

$$X_z=2\pi f3.19\times10^{-8}WT_f/4\Delta$$

where

$$T_f=\text{tooth face in inches} \tag{54}$$

If the stator slots break the magnetic circuit about the rotor bars, X_z probably will be smaller. Let

$$1/\Delta={}_0A+{}_2A\cos 2\pi x/\lambda+{}_4A\cos 4\pi x/\lambda+\ldots$$

Then

$${}_0D=R_B+j(X_s+2\pi f3.19\times10^{-8}WT_f\,{}_0A/4) \tag{55}$$

$${}_2D=j2\pi f3.19\times10^{-8}WT_f\,{}_2A/4 \text{ and so forth} \tag{56}$$

3. SKEW REACTANCE

$${}_nX_{ska}={}_nx_a(1-{}_nC_{sk})$$

$${}_nX_{skb}={}_nX_b(1-{}_nC_{sk})$$

$${}_nC_{sk}=[\sin(n\pi Sk/2\lambda)]/(n\pi Sk/2\lambda) \tag{57}$$

${}_nX_{ska}$ is to be added to the value of ${}_{nn}Z_a$ given in equation 49, and ${}_nX_{skb}$ is to be added to ${}_{nn}Z_b$ in equation 51.

Appendix II. Air-Gap Reactance

Figure 4 represents the flux wave.

Let

t_m=turns about stator tooth m
$p=0.4\pi2.54A_e/\Delta=3.19A_e/\Delta$
A_e=effective area per square inch of pole face

Total Reactance

$$\phi_m=WI\int_{x_{m-1}}^{x_m}t_m p\,dx$$
$$=\text{flux through } m\text{th tooth} \tag{58}$$

$$E_m=2\pi f\,10^{-8}t_mWI\int_{x_{m-1}}^{x_m}t_m p\,dx$$
$$=\text{voltage induced in winding about } m\text{th tooth}$$

$$X_t=(P/I)E_m=2\pi f\,10^{-8}PW\sum t_m^2\int_{m-1}^{x_m}p\,dx$$
$$=\text{total reactance linking primary and secondary} \tag{59}$$

If $p={}_0p+{}_2p\cos 2\pi x/\lambda$

$$X_t=2\pi f\,10^{-8}PW\sum t_m^2\left[\int_{m-1}^{x_m}{}_0p\,dx+\int_{x_{m-1}}^{x_m}{}_2p\cos(2\pi x/\lambda)dx\right]$$

$$X_l = 2\pi f 10^{-8} PW \sum t_m^2 [{}_0p(x_m - x_{m-1}) + ({}_2p\lambda/2\pi)(\sin 2\pi x_m/\lambda - \sin 2\pi x_{m-1}/\lambda)] \quad (60)$$

$$X_l = 2\pi f 10^{-8} PW \sum t_m^2 [{}_0p(x_m - x_{m-1}) + ({}_2p\lambda/\pi) \cos \pi(x_m + x_{m-1})/\lambda \times \sin \pi(x_m - x_{m-1})/\lambda] \quad (61)$$

Harmonic Reactance

${}_nB_a = {}_nB_{a\,\max} \cos n\pi x/\lambda$ = cosine component nth harmonic flux density

From the formula for Fourier coefficients, we find

$$ {}_nB_{a\,\max} = (4/\pi)I\,{}_na = (2/\pi) \times \sum \int_{x_{m-1}}^{x_m} I t_m p \cos (n\pi x/\lambda)\, d(\pi x/\lambda) \quad (62)$$

et

$$p = {}_0p + {}_2P \cos 2\pi x/\lambda$$

$$ {}_na = (1/2) \sum \int_{x_{m-1}}^{x_m} {}_0p \cos (n\pi x/\lambda) d(\pi x/\lambda) + (1/2) \sum \int_{x_{m-1}}^{x_m} 2p \cos (2\pi x/\lambda) \cos (n\pi x/\lambda) \times d(\pi x/\lambda)$$

$$ {}_na = ({}_0p/2n) \sum t_m(\sin n\pi x_m/\lambda - \sin n\pi x_{m-1}/\lambda) + [{}_2p/4(n+2] \times \sum t_m [\sin (n+2)\pi x_m/\lambda - \sin (n+2) \times \pi x_{m-1}/\lambda] + [{}_2p/4(n-2)] \sum t_m [\sin (n-2) \times \pi x_m/\lambda - \sin (n-2)\pi x_{m-1}/\lambda] \quad (63)$$

$$ {}_na = ({}_0p/n) \sum t_m \cos [n\pi(x_m + x_{m-1})/2\lambda] \sin [n\pi(x_m - x_{m-1})/2\lambda] + [{}_2p/2(n+2)] \sum t_m \cos [(n+2)\pi(x_m + x_{m-1})/2\lambda] \sin (n+2)\pi(x_m - x_{m-1})/2\lambda] + [{}_2p/2(n-2)] \sum t_m \cos [(n-2)\pi(x_m + x_{m-1})/2\lambda] \sin (n-2)\pi(x_m - x_{m-1})/2\lambda] \quad (64)$$

Likewise

${}_nB_b = {}_nB_{b\,\max} \sin n\pi x/\lambda$ = sin component, nth harmonic flux density

$$ {}_nB_{b\,\max} = (4/\pi)I\,{}_nb = (2/\pi) \sum \int_{x_{m-1}}^{x_m} I t_m p \sin (n\pi x/\lambda) d(\pi x/\lambda)$$

With aforesaid permeance, we find, as before

$$ {}_nb = ({}_0p/2n) \sum t_m (\cos n\pi x_{m-1}/\lambda - \cos n\pi x_m/\lambda) + [{}_2p/4(n+2)] \sum t_m [\cos(n+2) \times \pi x_{m-1}/\lambda - \cos (n+2)\pi x_m/\lambda] + [{}_2p/4(n-2)] \sum t_m [\cos (n-2)\pi x_{m-1}/\lambda - \cos (n-2)\pi x_m/\lambda] \quad (65)$$

$$ {}_nb = ({}_0p/n) \sum t_m \sin [n\pi(x_m + x_{m-1})/2\lambda] \sin [n\pi(x_m - x_{m-1})/2\lambda] + [{}_2p/2(n+2)] \sum t_m \sin [(n+2)\pi(x_m + x_{m-1})/2\lambda] \sin [(n+2)\pi(x_m - x_{m-1})/2\lambda] + [{}_2p/2(n-2)] \sum t_m \sin [(n-2)\pi(x_m + x_{m-1})/2\lambda] \sin [(n-2)\pi(x_m - x_{m-1})/2\lambda] \quad (66)$$

Let ${}_n\phi_{am}$ = cosine component of flux through mth tooth, nth harmonic.

$$ {}_n\phi_{am} = W \int_{x_{m-1}}^{x_m} {}_nB_a\, dx = W \int_{x_{m-1}}^{x_m} (4/\pi)I\,{}_na \times \cos (n\pi x/\lambda)dx$$

${}_nX_{am} = (2\pi f 10^{-8}/I) t_m\,{}_n\phi_{am}$ = reactance of mth tooth, nth harmonic, phase a

$$= 2\pi f 10^{-8}(4/\pi^2)W\lambda\,{}_na(t_m/n) \times (\sin n\pi x_m/\lambda - \sin n\pi x_{m-1}/\lambda)$$

Summing up the reactance for all the teeth, we have

$$ {}_nX'_a = 2\pi f\, 10^{-8}(8/\pi^2)PW\lambda\,{}_na\,{}_nt_a \quad (67)$$

where

${}_nX'_a$ = total mutual reactance, nth harmonic, phase a. The accent indicates that skew has not been deducted.

$$ {}_nt_a = (1/2n) \sum t_m(\sin n\pi x_m/\lambda - \sin n\pi x_{m-1}/\lambda) = (1/n) \sum t_m \cos [n\pi(x_m + x_{m-1})/2\lambda] \times \sin [n\pi(x_m - x_{m-1}]/2\lambda]$$

= nth harmonic of effective turns per pole, phase a (68)

Likewise it can be shown that

$$ {}_nX'_b = 2\pi f\, 10^{-8}(8/\pi^2)PW\lambda\,{}_nb\,{}_nt_b \quad (69)$$

$$ {}_nt_b = (1/2n) \sum t_m(\cos n\pi x_{m-1}/\lambda - \cos n\pi x_m/\lambda) = (1/n) \sum t_m \sin [n\pi(x_m + x_{m-1})/2\lambda] \times \sin [n\pi(x_m - x_{m-1})/2\lambda]$$

= effective turns per pole, nth harmonic, phase b (70)

Each of these reactances must be corrected for skew

$$ {}_nX_a = {}_nX'_a\,{}_nC_{sk} = {}_nX'_a [\sin (n\pi Sk/2\lambda)] + (n\pi Sk/2\lambda)$$

$$ {}_nX_b = {}_nX'_b\,{}_nC_{sk} \quad (71)$$

The amount of reactance that is deducted by this means is added to both the primary and secondary leakage reactance, as in equation 57.

It is convenient to take as the origin a point $\lambda/4$ from the center of the pole, as in Figure 1.

Appendix III. Sample Calculations for a Representative Motor

Partial List of Design Data—Dimensions in Inches

$D = 1.75$
$D_r = 1.65$
$W = 1.25$
$P = 4$
$\lambda = \pi 1.75/4 = 1.375$
$S_s = 0.00815$
$S_r = 0.02445$
$S_s = 25$
$t_1 = 250$
$x_0 = -\lambda/8$
$x_1 = 5\lambda/8$
$\Delta_{\min} = 0.015$
$\Delta_{\max} = 0.045$
$A_s = 0.815$
$T_j = 0.179$
$K_s = 1.36$
$R_1 = 22$ ohms
$X_1' = 10.0$ ohms
$Sk = 4\lambda/25 = 0.22$

Δ varies as in equation 1; stator iron is continuous.

Primary Reactances

$$ {}_0A = (1/2)(1/0.015 + 1/0.045) = 44.5$$
$$ {}_2A = (1/2)(1/0.015 - 1/0.045) = 22.45 \quad (1)$$

$$ {}_0p = 44.5 \times 0.815 \times 3.19 = 115.6$$
$$ {}_2p = 22.25 \times 0.815 \times 3.19 = 57.8$$
$$X_l = 377 \times 10^{-8} \times 4 \times 1.25 \times 250^2 \times [115.6(5\lambda/8 - \lambda/8) \times 1.375 + 22.5(1.75/4) \cos (\pi/\lambda)(5\lambda/8 + \lambda/8) \times \sin (\pi/\lambda)(5\lambda/8 - \lambda/8)] = 140.7 \text{ ohms} \quad (61)$$

$$ {}_1a = 250 \begin{bmatrix} 115.6 \cos (\pi/2\lambda)(5\lambda/8 - \lambda/8) \times \\ \sin (\pi/2\lambda)(5\lambda/8 + \lambda/8) \times \\ (57.8/2 \times 3) \cos (3\pi/2\lambda)(5\lambda/8 - \\ \lambda/8) \sin (3\pi/2\lambda)(5\lambda/8 + \lambda/8) - \\ (57.8/2) \cos (\pi/2\lambda)(5\lambda/8 - \lambda/8) \times \\ \sin (-\pi/2\lambda)(5\lambda/8 + \lambda/8) \end{bmatrix} = 24{,}150 \quad (64)$$

$$ {}_1t_a = 250 \cos (\pi/2\lambda)(5\lambda/8 - \lambda/8) \times \sin (\pi/2\lambda)(5\lambda/8 + \lambda/8) = 163.2 \quad (57)$$

$$ {}_1X'_a = 377 \times 10^{-8}(8/\pi^2)4 \times 1.25 \times 1.375 \times 163.2 \times 24{,}150 = 83.0 \text{ ohms} \quad (67)$$

$$1 - C_{sk} = (1/6)(\pi/2\lambda)^2(4\lambda/25)^2 = 0.0105 \quad (21)$$

$$ {}_{ska} = 0.0105 \times 83.0 = 0.872 \text{ ohm} \quad (19)$$
$$ {}_1X_a = 83.0 - 0.872 = 82.1 \text{ ohms}$$

Secondary Impedance

$$R_s = \sqrt{1.25^2 + 0.22^2} \times 0.692 \times 10^{-6}/0.00815 = 1.076 \times 10^{-4} \quad (51)$$

$$R_r = 0.692 \times 10^{-6}/0.02445 = 0.2825 \times 10^{-4} \quad (52)$$

$$K = 4 \times 0.2825 \times 10^{-4} \times 1.65 \times 25/16 = 0.926 \times 10^{-4} \quad (47)$$

$$X_s = 377 \times 3.19 \times 10^{-8} \times 1.25 \times 1.36 = 0.293 \times 10^{-4} \quad (53)$$

$$ {}_0D = 1.076 \times 10^{-4} + j(0.293 + 377 \times 10^{-4} \times 3.19 \times 1.25 \times 0.179 \times 44.5/4)10^{-4} = (1.076 + j\,0.502) \times 10^{-4} \quad (55)$$

$$ {}_2D = j\,377 \times 10^{-8} \times 3.19 \times 1.25 \times 0.179 \times 22.25/4 = j\,0.1495 \times 10^{-4} \quad (56)$$

$$ {}_{11}G_a = 4 \times 16 \times 163.2^2/25 = 6.83 \times 10^4 \quad (48)$$

$$ {}_{11}Z_a = 6.83[(2 \times 1.076 + 0.926) + j(2 \times 0.502 - 0.1495)] - j\,0.872 = 21.05 + j\,6.71 \quad (49)$$

Computation of the other constants is similar.

Summary of Constants

${}_1X_a = 82.1$ ohms	${}_3X_a = 3.327$ ohms
${}_1X_{2a} = \underline{6.71}$	${}_3X_{2a} = \underline{1.519}$
${}_1X_{0a} = 88.81$	${}_3X_{0a} = 4.846$
${}_1X_b = 45.72$	${}_3X_b = 1.073$

${}_1X_{2b}=\underline{8.37}$ ${}_3X_{2b}=\underline{1.286}$

${}_1X_{ab}=54.1$ ${}_3X_{ab}=2.359$

${}_1R_2=21.05$ ${}_3R_2=2.64$

$R_1=22$ ohms

$X_1'=10$

$X_t=140.7$

${}_1X_a-{}_1X_b=36.38$

${}_1X_{2a}-{}_1X_{2b}=1.665$

${}_3X_a-{}_3X_b=2.254$

${}_3X_{2a}-{}_3X_{2b}=-0.233$

Starting Conditions

$${}_1K_{ra}=(82.1/88.8)^2/(1+(21.05/88.8)^2)$$
$$=0.810$$
$${}_1K_a=(0.810/82.1)(88.8+j21.05)$$
$$=0.875-j0.208 \qquad (12)$$

Likewise

$${}_1K_b=0.735+j\,0.2855,\quad {}_3K_a=0.529+j\,0.288,$$
$${}_3K_b=0.2025+j\,0.2265$$

The total impedance is

$$\begin{aligned}
& & 22 \quad +j\;150.7\\
-j\,82.1\;(0.875+j\,0.208) &= & 17.1\;-j\;\;71.9\\
-j\,45.72(0.735+j\,0.2855) &= & 13.08-j\;\;33.6\\
-j\;3.33(0.529+j\,0.288) &= & 0.96-j\;\;1.76\\
-j\;1.07(0.2025+j\,0.2265) &= & \underline{0.23-j\;\;0.22}\\
& & 53.27+j\;\;43.1\\
& & =68.5\underline{/39^\circ 00'}
\end{aligned}$$

$$I=110/68.7\underline{/39^\circ 00'}=1.60\underline{/-39^\circ 00'}\text{ amperes}$$

Power factor $=\cos 39^\circ 00'=0.778$

$W=110\times1.60\times0.778=137$ watts, input

$${}_1T=(11.3\times4\times0.810\times0.618/60\times82.1\times 45.72)1.6^2[1.665(21.05^2-88.8\times 54.1)-2\times36.38\times21.05^2]$$
$$=0.000256(1.665\times(-4{,}368)-2\times 36.38\times442)$$
$${}_1T=0.000256(-7{,}270-32{,}200)$$
$$=-10.12\text{ ounce-inches} \qquad (13)$$

The greatest contribution to ${}_1T$ in this case is seen to be from ${}_1R_2^2({}_1X_a-{}_1X_b)$. In the same manner

${}_3T=-1.632$ $\quad T={}_1T+{}_3T=-11.7$ ounce-inches

Although the third harmonic does not affect the current greatly, it is seen to contribute appreciably to the torque. Using the more complete formula of equation 45, $T=-10.6$ ounce inches.

Running Conditions

Let

$S=-0.85 \qquad 1-S^2=0.2775$

$$\begin{aligned}
-0.85\times21.05\times45.72 &= -\;\;818\\
-0.2775\times54.1\times82.1 &= \underline{-1{,}234}\\
{}_1F_a &= -2{,}025+j\,1{,}730\\
j\,21.05\times82.1 &= j\,1{,}730 \qquad (6)
\end{aligned}$$

$$\begin{aligned}
-(-0.85)\times21.05\times82.1 &= \;\;1{,}470\\
-0.2775\times88.8\times45.7 &= \underline{-1{,}125}\\
{}_1F_b &= \;\;345+j\,963\\
j\,21.05\times45.7 &= j\,963 \qquad (7)
\end{aligned}$$

$$\begin{aligned}
21.05^2 &= \;\;443\\
-0.2775\times88.8\times54.1 &= \underline{-1{,}333}\\
{}_1F &= -\;\;891+j\,3{,}010\\
j\,21.05(88.8-54.1) &= j\,3{,}010 \qquad (8)
\end{aligned}$$

$${}_1K_a=(-2{,}052+j\,1{,}730)/(-891+j\,3{,}010)$$
$$=0.714-j\,0.470 \qquad (5)$$

$${}_1K_b=(345-j\,963)/(-891+j\,3{,}010)$$
$$=0.262-j\,0.193 \qquad (6)$$

If ${}_3K_a$ and ${}_3K_b$ are computed and equation 9 is used again

$Z_0=93.1\underline{/56^\circ 20'}$

$I=1.18\underline{/-56^\circ 20'}$ amperes $=0.653-j\,0.981$ ampere

Power factor $=0.55$

Input $=71.4$ watt (iron losses to be added)

${}_1U=0.714 \qquad {}_1u=0.470$

${}_1V=0.262 \qquad {}_1v=-0.193$

$${}_1U_1V-{}_1u_1v=0.714\times0.262-0.470\times 0.193=0.0961$$
$${}_1T=(11.3\times4/60)1.18^2[(0.262-0.0961)\times 82.1-(0.714-0.0961)45.7]=-15.3$$

Likewise

${}_3T=2.95$

$T={}_1T+{}_3T=-12.3$ ounce inches

Output $=0.001782\times0.85\times12.3=0.0186$ horsepower

Output $=0.0186\times746=13.9$ watts

Efficiency(disregarding iron losses) $=13.9\times 71.4=19.5$ per cent

Iron Losses

Let

Hysteresis coefficient, $\eta=0.003$

Conductivity of iron $=10^5$

Thickness of laminations $=0.03$

Cross section of half of flux path $=0.390$

Volume of iron $=10.3$

Maximum flux density $=B_m$

Hysteresis loss per cubic inch $={}_hw$

Eddy-current loss per cubic inch $={}_ew$

$${}_hw=8.3\times10^{-8}\times0.003\times60B_m{}^{1.6}=1.495\times 10^{-8}B_m{}^{1.6}\text{ watts per cubic inch}$$
$${}_ew=4.18\times10^{-16}\times10^5\times0.03^2\times60^2B_m{}^2$$
$$=1.353\times10^{-10}B^2\text{ watts per cubic inch}$$

To obtain the voltage drop due to the flux, deduct the primary IR (current resistance) drop from the applied voltage

$$E_\phi=110-22\times1.18\underline{/-56^\circ 20'}$$
$$=98.1\underline{/12^\circ 44'}$$
$$\phi_p=E\phi/2\pi f\,10^{-8}t_p\;P=\text{flux per pole}$$
$$\phi_p=98.1/377\times10^{-8}\times250\times4=2.60\times10^4$$
$$B_m=2.60\times10^4\times\sqrt{2}/2\times0.390=4.71\times10^4\text{ lines per square inch}$$
$${}_hw=1.495\times10^{-8}(4.71\times10^4)^{1.6}=0.449$$
$${}_ew=1.353\times10^{-10}(4.71\times10^4)^2=\underline{0.301}$$
$$0.750\text{ watt per cubic inch}$$

$W=10.3\times0.750=7.73$ watts, total iron loss

$$7.73/98.1\underline{/12^\circ 44'}=0.0788\underline{/-12^\circ 44'}$$
$$=0.0770-j\,0.0146=\text{iron-loss current}$$
$$0.653-j\,0.981+0.0770-j\,0.0146=0.730-j\,0.996=1.24\underline{/-53^\circ 45'}=\text{total current}$$

$71.4+7.8=79.2$ watts, total input

$13.9/79.2=17.9$ per cent efficiency

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An Eight-Year Investigation of Lightning Currents and Preventive Lightning Protection on a Transmission System

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SOME 10 or 12 years ago effective lightning protection began to be applied to transmission lines, and, about the same time, an inexpensive measuring device (the surge-crest ammeter) was developed.[1] On one transmission system this low-cost device has been applied to every structure in entire lines and has produced definite numerical evidence of the performance of the protection provided.

It is shown that on the system studied, the distribution of lightning strokes occurs at random, and that there are great variations in the number and intensity of strokes from year to year. The numerical data show that the gain would be small from protection of single structures or sections of lines. The data also make it possible to estimate long-time line performance from short-time records. The surge impedance of an overhead ground wire is indicated to be less than 235 ohms instead of the generally accepted value of 400 to 500 ohms

The counterpoise arrangements on the lines in question vary from a single wire to eight wires in multiple. There is no evidence to show that one arrangement is better than another, as long as the resulting ground-resistance value is the same. The bonding of structures at crossings and parallels shows no tendency to spread trouble from one structure to another. Arresters connected to well-protected lines were seldom caused to operate, and, when they did, the currents in most cases were small.

Scope of Field Investigation

The installation of surge-crest-ammeter links was begun on a system in Pennsylvania and Maryland in June 1934, with additional installations being made every succeeding year. By the end of 1942, brackets were installed on transmission-line steel structures, lightning arresters, overhead ground wires, lightning rods, and a steel flagpole. The aggregate exposure of the links has been considerable on the lines, amounting to 13,227 tower-years and 1,776 mile-years with 5,501 bracket records obtained. The exposure on the lightning arresters connected at the terminals of the lines has been 155 bank-years, with 54 records obtained. No significant records were obtained from the few miscellaneous bracket installations.

Table I. Variation in Total Number of Strokes Recorded Yearly on Five Transmission Lines

Year	Strokes Recorded	Ratio to Five-Year Average
1938	239	1.05
1939	283	1.25
1940	252	1.11
1941	163	0.72
1942	199	0.88
Average	227	

Frequency of Strokes to Lines

Two 220-kv lines, one 132-kv line, and two 66-kv lines cover the area served by the system. The stroke records from these five lines, having an aggregate length of 228 miles, are taken as giving a representative picture of lightning conditions on the system. Table I shows how the number of strokes recorded on these lines varied from year to year. The ratio of the yearly maximum of recorded strokes to the minimum is 1.74.

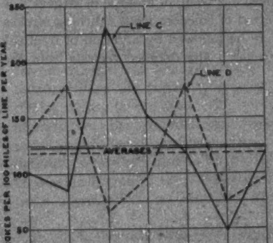

Figure 1. Strokes to two 66-kv lines

When the records of individual lines are examined, the variations become more pronounced. This is demonstrated in Table II, which is the record for the four transmission lines on which tower lightning currents have been measured for the longest time. As is to be expected, the range of yearly variations is greater for single lines than for the system as a whole. Also, the longest line does not show so wide a range in the number of recorded strokes as do the shorter lines. Beyond these generalizations, the data show random behavior. For instance, in Figure 1 are given records of two comparable lines, both lying in a more or less east–west direction and having very nearly the same stroke density. In 1938 line *C* received its peak number of strokes for its seven years of record, while line *D* received the lowest number of strokes for the same time.

These wide variations are indicative of the difficulty of judging the value of any lightning-protective scheme from short-time field-performance data. Table III shows how the average recorded stroke frequency for a small number of consecutive years can be quite far from representative of the long-time experience. In this table, seven- and eight-year records are assumed to be representative samples of the long-time record. This assumption seems reasonable for all practical purposes.

As was to be expected, the average stroke frequency for any number of consecutive years usually did not deviate from the period average as widely on line *A*, 91.8 miles long, as it did on the shorter lines. A long line has a good chance of being in the path of most local thunderstorms, some of which might miss shorter lines.

In Table III, the large ratios of maximum to minimum number of strokes recorded yearly help explain the common experience of a line behaving well one year and poorly the next.

Frequency of Strokes to Individual Towers

Repeated lightning trouble at a given tower during a relatively short period of one or two years often is considered indicative of a location particularly sub-

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Table II. Recorded Strokes to Lines Per 100 Miles Per Year

	Line A 220 Kv	Line B 132 Kv	Line C 66 Kv	Line D 66 Kv	Average Values
Length of line in miles	91.8	33.8	29.4	22.8	
Period of record in years	8	8	7	7	
Average strokes for period	95	135	123	119	118
Maximum in one year	129	219	232	180	190
Ratio to period average	1.36	1.62	1.89	1.51	1.61
Minimum in one year	59	86	48	66	65
Ratio to period average	0.62	0.64	0.39	0.55	0.55
Ratio of yearly maximum to yearly minimum	2.19	2.54	4.85	2.73	2.93

1,364 mile-years of line are represented by above record.

System average strokes per 100 miles of line per year is 113 for five observed lines totaling 1,617 mile-years of experience.

Table III. Ratio of Average Stroke Frequency for Consecutive Years to the Average for an Entire Period of Record

		Period of Record (Years)			
		8	8	7	7
Indicated Number of Consecutive Years		Line A	Line B	Line C	Line D
One year records	Maximum	1.36	1.62	1.89	1.52
	Minimum	0.62	0.64	0.39	0.56
Averages for two consecutive years	Maximum	1.29	1.36	1.57	1.34
	Minimum	0.77	0.70	0.68	0.68
Averages for three consecutive years	Maximum	1.18	1.31	1.37	1.07
	Minimum	0.82	0.76	0.79	0.96
Averages for four consecutive years	Maximum	1.06	1.28	1.20	1.10
	Minimum	0.85	0.86	0.90	0.88
Averages for five consecutive years	Maximum	1.10	1.19	1.13	1.11
	Minimum	0.89	0.93	1.04	0.87
Averages for six consecutive years	Maximum	1.08	1.10	1.03	1.03
	Minimum	0.91	0.96	1.00	1.03
Averages for seven consecutive years	Maximum	1.02	1.05		
	Minimum	0.95	1.04		

ject to lightning. Inasmuch as the records on lines *A* and *B* have been kept for eight complete years, some data are available which show what can be expected. Table II shows that a unit length of line *A* is struck, on the average, about 70 per cent as often as a unit length of line *B*. The effect of the greater number of strokes to line *B* is shown in the greater percentages of towers struck and struck more frequently (Table IV). The percentages of towers of both lines that were struck one, two, and three times are nearly equal.

There were 104 cases on lines *A* and *B* during the eight years of record in which an individual tower was struck two years in succession, 15 cases in which a tower was struck three consecutive years, and only three cases in which individual towers were struck four years in succession.

Towers rarely were struck more than once in a single year. On lines *A* and *B* there were 63 cases in which towers were struck twice in a single year, and only one case in which three strokes to an individual tower were recorded.

Table V shows that towers can pass through several years without being struck. If this should occur where no lightning-recording device is used, it might be inferred that the lightning protection is quite good, when in reality there has been an absence of strokes. A period of freedom from lightning strokes may be followed by a period of frequent strokes. The detailed records from the towers of these two lines show all types of distribution of strokes during the eight-year period. From these data it appears that no tower is more subject to lightning than another except by chance.

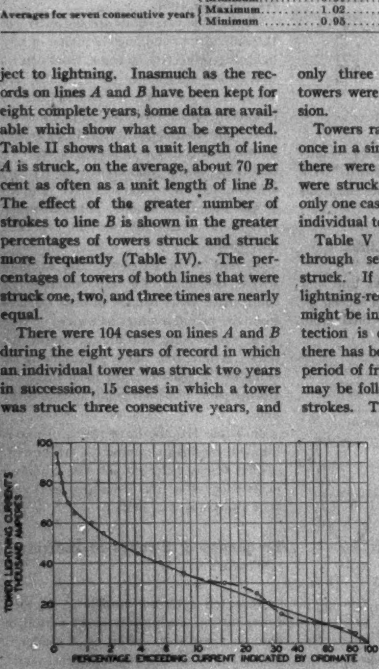

Figure 2. Distribution of 1,987 currents measured in towers believed struck by lightning

Variations in Lightning Intensity

In addition to the variation in the number of strokes striking a line, a variation in their intensity also affects performance. Figure 2 shows the distribution of tower currents during a long period of time.

In any one year the percentage of tower currents exceeding any given value may be quite different from the average for a long period. This is shown in Table VI. Since both the stroke frequency and intensity seem to be random phenomena, a number of years sometimes elapses between flashovers on a fairly well-protected line that is faulted by only high-intensity strokes. The other extreme condition, in which there are a number of flashovers in one year, or even during one storm, sometimes occurs. A rare instance of this latter type of trouble occurred during 1942 on the 66-kv line *C*, when five towers covering a distance of 1.9 miles experienced flashovers, although only one simultaneous double-circuit tripout occurred.

An interesting and perhaps significant phenomenon appears in Figure 2. For some years on this distribution curve of tower currents, the points for 30,000 amperes and less have followed a wavy line such as shown by the broken line in Figure 2. Up to this time,[2] the apparent irregularity in the points was thought to be the result of insufficient data, but now, with 1,987 cases accumulated, this original explanation appears to be incorrect. All of the data obtained thus far (1934–42) have been plotted in 5,000-ampere groups in Figure 3. It will be seen that the maximum number of cases has occurred in the 5,000- to 10,000-

Table IV. Number of Times Individual Towers Have Been Struck by Lightning During an Eight-Year Period

Number of Times Struck	Towers Struck the Indicated Number of Times			
	Line A		Line B	
	Number	Per Cent	Number	Per Cent
0	115	24.1	23	12.9
1	163	34.2	63	35.2
2	106	22.2	41	22.9
3	71	14.9	26	14.5
4	15	3.2	19	10.5
5	5	1.0	4	2.3
6	2	0.4	2	1.1
7	0	0	1	0.6
	477		179	

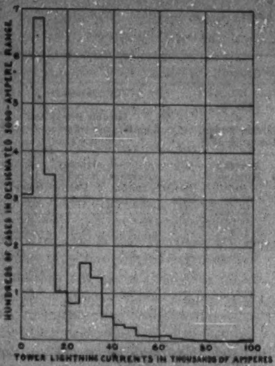

Figure 3. Range distribution of 1,987 currents in towers believed struck by lightning 1934–42

Table VII. Ratio of Sum of Four Leg Currents, Representing Probable Tower Current, to Four Times the Single-Leg Current

Tower Current (Four Times Single-Leg Current)	Number of Leg Records	Ratio of Sum of Four Leg Currents to Four Times Single-Leg Current		
		Average	Median	Range
1– 1,000	28	5.39	2.42	1.42–49.5
1,001– 2,000	21	3.78	2.69	0.93–17.8
2,001– 5,000	46	1.24	1.07	0.25– 8.75
5,001– 7,500	48	0.89	0.82	0.25– 2.88
7,501–10,000	59	1.13	1.02	0.25– 4.13
10,001–15,000	87	1.01	0.97	0.30– 1.85
15,001–20,000	17	1.17	0.91	0.33– 4.25
20,001–25,000	13	0.89	0.88	0.28– 1.26
25,001–30,000	23	0.82	0.84	0.47– 1.10
30,001–35,000	17	0.80	0.82	0.26– 1.37
35,001–40,000	9	1.03	1.01	0.73– 1.28
40,001–45,000	1	1.13	1.13	
45,001–50,000	4	0.89	0.89	0.82– 0.96
50,001–55,000	4	0.85	1.00	0.25– 1.14
55,001–60,000	5	0.94	1.01	0.77– 1.03
60,001–65,000	3	0.97	0.97	0.93– 1.02
65,001–70,000	2	0.93	0.93	0.88– 0.98
76,000	1	0.52	0.52	
80,001–85,000	2	0.37	0.37	0.25– 0.48
114,000	1	0.57	0.57	
	391			

ampere range, with a secondary peak occurring in the 25,000- to 30,000-ampere range. To investigate whether or not this phenomenon is the result of some unusual records obtained during the early part of the period of study which influenced the entire set of data, the data from individual years in which records were complete were plotted by percentage in Figure 4. The primary peak is quite pronounced every year in the 5,000- to 10,000-ampere range. The secondary maximum in the 25,000- to 30,000-ampere range is quite apparent in the records from 1935, 1938, 1940, 1941, and 1942. It is less prominent in the 1937 and 1939 records. The secondary maximum occurs in the 20,000- to 25,000-ampere range in 1936.

Table V. Towers Not Struck in Successive Years During an Eight-Year Period

Years in Succession in Which a Tower Was Not Struck	Number of Cases	
	Line A	Line B
1	200	122
2	167	65
3	133	62
4	98	40
5	67	27
6	57	19
7	38	13
8	115	23

Table VI. Range of Per Cent of Tower Currents in Single Years Exceeding Indicated Value

Tower Amperes	Range of Yearly Per Cent*		Average Per Cent 1934–42
	Minimum	Maximum	
10,000	37.5	65.4	50
20,000	13.4	39	27.1
30,000	6.2	20	14.8
40,000	0.8	9.2	5.5
50,000	0	4.1	2.8
60,000	0	2.0	1.2
70,000	0	1.2	0.4
80,000	0	0.8	0.25
90,000	0	0.6	0.15
100,000	0	0	0

* Eight-year record, 1935–42. Records were not obtained for the entire 1934 lightning season, but the records obtained in that year are included in the average values given in the last column.

This analysis makes it appear that the wavy form of the broken line curve in Figure 2 is a more accurate representation of the distribution of tower currents below 35,000 amperes than is the straighter line. It appears that there are two principal types of lightning discharges as far as tower currents are concerned. Most of one type produce tower currents in the 5,000- to 10,000-ampere range, and most of the other type produce them in the 25,000- to 30,000-ampere range.

Division of Lightning Currents in Legs of Towers

Most of the tower lightning currents have been measured in towers where surge-crest-ammeter brackets have been mounted on only one leg. The tower current in such cases has been assumed to be four times the current measured in the leg having the bracket. Of principal interest are the records from towers which it is believed have been struck by lightning or are adjacent to struck spans of overhead ground wire, because it is these towers which are subject to flashovers.

It is believed that there have been 104 times when towers provided with surge-crest-ammeter brackets on all four legs have been hit. These 416 records permit an estimate to be made of the errors involved when the tower current is computed from the current in a single leg.

Each of the 104 sets of records has been analyzed by first adding the four leg currents to obtain the nominal tower current. Then, each record was treated as if it were the only one available from a tower. In Table VII are data on the ratio of the sum of the four leg currents in a tower to the product of four times the single leg currents taken individually. If one assumes that the sum of the four leg currents is a good representation of the true tower current, the probable tower current can be obtained from a single-leg record by the relation:

Probable tower current = 4 × leg current × ratio from Table VII

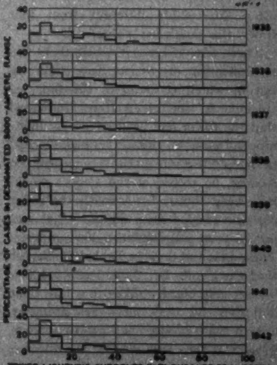

Figure 4. Range distribution of tower lightning currents by years

Table VIII. Lightning Disturbances on Parallel 66-Kv Lines

40 Miles—415 Towers

	Observation Period (Years of Record)											
	1918–31 14 (No Grounding)			1932–34 3 (Single Bonds Towers 6–90)			1935–38 4 (Single Bonds Towers 6–332)			1939–42 4 (Double Bonds and Counterpoise—Towers 6–415—Entire Line)		
	Number During Period	Average Number Per Year	Per Cent	Number During Period	Average Number Per Year	Per Cent	Number During Period	Average Number Per Year	Per Cent	Number During Period	Average Number Per Year	Per Cent
Disturbances involving one circuit	91	6.5	46.5	11	3.7	31.4	9	2.3	47.4	2	0.5	50
Disturbances involving two circuits on only one line	95	6.8	48.5	15	5.0	42.9	8	2	42.1	0	0	0
Disturbances involving circuits on both lines	10	0.7	5.0	9	3.0	25.7	2	0.5	10.5	2	0.5	50
Totals	196	14	100	35	11.7	100	19	4.8	100	4	1	100

For instance, there was a case of flashover in which the tower lightning current was calculated from one leg-current record as being 13,400 amperes. This current lies in the range 10,001 to 15,000 amperes in Table VII. The median ratio is 0.97 in this range, which indicates that there is an even chance that the tower current was more or less than 0.97×13,400 amperes = 13,000 amperes. However, experience with 87 records in this range indicates that the current possibly may have been anywhere in the range 0.30 to 1.85×13,400 amperes, or between 4,000 and 24,300 amperes.

The values of the median ratio being near unity in most cases in Table VII indicates that, on the average, results throughout most of the range of the currents measured are of fairly uniform accuracy. The low values of the median ratio for tower currents above 70,000 amperes and the high values below 2,000 amperes suggest that currents indicated in these ranges by single-leg records are often appreciably different from four times the single-leg value.

There were 25 cases in which there was no indication of current in a tower leg but in which the sum of the four leg currents in the given tower indicated that the tower had been struck by lightning. In these cases, if brackets had been mounted only on the legs yielding no records, no record of the strokes would have been obtained. Actually the tower currents ranged from 800 to 6,200 amperes, averaging about 2,200 amperes. These data and those in Table VII suggest that there may be 25/(416−25)×100 = 6.4 per cent more strokes to a line than indicated by the surge-crest-ammeter links if there is only one bracket on each tower. It thus appears that the absence of a record on a tower having a single bracket does not mean necessarily that the tower has not been struck by lightning. The data also imply that it probably would be quite difficult to arrive at a satisfactory constant factor to account for currents carried by tower cross members, because the current distribution in cross members probably varies as widely as that in the legs.

Effect on Flashovers of Bonding Adjacent Towers

There have been three instances of flashover on neighboring towers where footings are bonded with buried conductors. Two of these were simultaneous flashovers on two lines carrying two circuits each and situated on the same right of way with 50 feet separation. Table VIII summarizes the lightning experience on these lines in the 25-year period 1918–42. During this period the lines operated 14 years without bonding. When adjacent towers on about three quarters of the line were bonded, the four-year period 1935–38 showed only 4.8 disturbances per year as compared to an average of 14 in the period without bonding.

Before the bonding was installed there were 0.7 disturbance per year involving circuits on both lines, and after the bonding was installed there were 0.5 disturbance per year of this type.

The third instance of flashover occurred on a tower of line *C*, which is shielded by the much taller tower of line *B*. The footings of the two towers are bonded together. When flashover occurred on the line-*C* tower, there was no flashover of the line-*B* tower, although the latter tower current was about 80,000 amperes. This current gives a tower-footing resistance drop of around 930-kv. Apparently the 132-kv tower was struck, but the potential of the 66-kv tower was raised through the bonding to a level sufficient to flash over. During the period of record previous to the flashover in 1942, there were ten strokes to the towers at this intersection without any flashover. The tower currents were all 38,000 amperes or less.

These three instances of trouble at bonded towers raise a question about the value of bonding neighboring transmission towers, but the complete record of tower lightning currents makes it appear that bonding is helpful in reducing the total number of flashovers on adjacent towers if the bonding can reduce footing resistances. There seems to be no effect of bonding on multiple-line disturbances. Table VIII shows the total number of disturbances reduced further by the installation of counterpoise. The number of simultaneous flashovers appears to remain constant, but becomes a greater percentage of the total number of disturbances with improvement of grounding.

Efficacy of Continuous Counterpoise

Examination of Table IX shows that as many as eight continuous counterpoise

Table IX. Flashovers Believed Caused by Strokes to Towers or Overhead Ground Wires on 66-Kv Lines Provided With Preventive Protection

Type of Counterpoise	Number of Cases	Tower Footing (Ohms)	Ratio of Footing Resistance Drop to Tower Flashover Level
8 continuous	2	3.6, 5	0.26, 0.37
6 continuous	2	10.5, 17	0.75, 0.91
5 continuous	2	4.6, 9	0.26, 0.38
4 continuous	2	13.8, 19	0.82, 1.16
4 continuous 3 400 feet+	2	11.8, 17.2	1.25, 1.30
2 continuous 3 400 feet+	1	5.4	0.38
	11		

There has been one flashover on a 132-kv tower (137 feet high):

Footing (ohms) 12.2

$$\frac{\text{Footing resistance drop}}{\text{Tower flashover level}} = 0.41$$

Counterpoise: Four continuous+4 300 feet connected to a 66-kv tower do not produce a surge impedance low enough to prevent flashovers. These lines have insulator flashover values of 565–635 kv with a $1\frac{1}{2} \times 40$-microsecond wave. In a number of cases the ground resistance was quite low, making the tower potentials based on the product of tower current times tower-footing resistance only a small fraction of the value expected to produce flashover. Since flashover occurred with such low footing resistance it seems reasonable that the inductive reactance drop was largely responsible and that the footing resistance might be higher without additional flashovers occurring. Measurements show that the omission of the four inner conductors of the counterpoise system, where the ground resistance is 3.6 ohms, would raise the ground resistance of the remaining counterpoise, consisting of four conductors, to about 4.5 ohms. The inner conductors were originally installed in the hope that the surge impedance would be reduced sufficiently to prevent flashovers. The experience listed in Table IX shows that this hope was not realized. It thus appears that no benefit is derived from adding counterpoise wires after the ground resistance has been reduced to the optimum value.[8]

Line *B* has an insulator flashover level of 1,015 kv. The only flashover on this line was at a tower 137 feet high, which is about twice the height of the 66-kv towers. It seems likely that the surge impedance added by the greater tower height contributed to the conditions causing flashover. The product of tower-footing resistance times tower current is only 41 per cent of the theoretical tower potential required for flashover. The eight-year record of line *B* indicates that multiple counterpoise systems having the optimum ground resistance of ten ohms or less prevented flashovers.

The 220-kv lines have an insulator flashover level of 1,605 kv, for which the optimum footing resistance is 16 ohms.[8] Since very low footing resistances were not required, an experimental installation of single counterpoise, continuous in both directions to the next adjacent towers, was made at 38 towers of a 220-kv line. The lightning-current records have been kept on these towers for nine years, making a total of $9 \times 38 = 342$ tower years of record without flashover. Sixty-four strokes were recorded on the towers during this period, producing tower currents as great as 98,000 amperes. The footing resistances did not exceed 20 ohms. The data seem to indicate that towers having an insulator flashover level of about 1,600 kv are quite well protected by single continuous counterpoise, provided this type of grounding will bring the footing resistances down to the optimum value.

Estimated Maximum Apparent Surge Impedance of an Overhead Ground Wire

Table X gives 17 instances when tower currents of about 30,000 amperes or greater have been recorded in two adjacent towers of a line and the currents have not differed from each other by more than 25 per cent of the larger tower current. A pair of records of this type has been interpreted as resulting from a stroke to an overhead ground wire about midway between towers. The current in the overhead ground wire is probably as great as, or greater than the indicated tower current, because some current probably flows in the overhead ground wire, or wires, past the recording tower and does not affect the surge-crest-ammeter links.

Since no tripouts occurred in any of these cases, there is assurance that no flashovers occurred at any of these locations. Incidentally, flashovers causing tripouts have been at towers, not in mid-span. In Table X the voltage required to flash over the mid-span spacing and the probable minimum value of the lightning current in the overhead ground wire are given. The probable upper limit for the surge impedance of the overhead ground wire is the foregoing voltage divided by the corresponding tower current. The average value of the surge impedance, calculated in this way, is 236 ohms, the median is 228 ohms, and the range is from 139 to 323 ohms. These values for the surge impedance of a conductor in air are appreciably less than the theoretical value of 400 or 500 ohms often quoted. Of more practical interest is the implication from these data that mid-span clearances may not need to be so great as often are allowed. Perhaps the prevention of sleet and snow troubles, rather than the prevention of mid-span lightning flashovers, should be the controlling factor in determining mid-span clearances.

Table X. Estimate of Maximum Value of Surge Impedance of an Overhead Ground Wire

Mid-Span Spacing Between Overhead Ground Wire and Nearest Line Conductor	Span Length in Feet	Nominal Voltage (Kv) to Produce Flashover at Mid-Span (1 Microsecond Front)	Lightning Currents in Adjacent Towers	Maximum Surge Impedance (Ohms) Without Flashover
15.8	810	5,020	36,200 35,200	139
23.8	780	7,560	33,200 30,000	228
27.2	1,020	8,650	30,000 30,000	289
28.5	790	9,060	41,600 41,200	223
28.5	915	9,060	34,800 31,200	261
29.5	1,250	9,380	40,400 39,200	232
29.9	1,100	9,500	43,200 38,000	220
30.1	980	9,560	47,200 39,200	203
30.1	1,000	9,560	34,800 31,200	275
30.1	1,000	9,560	48,800 40,000	196
30.1	1,000	9,560	47,600 38,200	200
31.6	1,150	10,050	42,800 33,600	235
32.1	1,210	10,200	35,000 30,400	292
32.8	1,250	10,400	37,600 30,600	276
33.3	1,290	10,600	32,800 29,200	323
33.8	1,320	10,750	53,200 42,400	202
34.2	1,750	10,900	50,400 44,400	216

Comparison of Estimated and Actual Lightning Performance

Published literature on lightning protection indicates over 20 factors probably affecting the estimated lightning performance of a transmission line (see appendix). The individual effect of most of these factors is known only qualitatively, and so it is practically hopeless to include them in any estimating method. Relatively simple calculations were attempted in 1937 for estimating the line interruptions which might be expected as a result of lightning.[2,4] From the records available,

Table XI. Estimated and Actual Lightning Outages

Line	Years of Record	Lightning Outages Per Year		Error (Per Cent)
		Actual	Estimated	
A—220 kv	11	0.09	0.014	**
F—220 kv	5	0	0*	**
B—132 kv	8	0.13	0.05	**
E— 66 kv	4	1	1	0
C— 66 kv	5	1	1.3	+30
Before improvement (1928–36)	9	17.9	21.0	+17
D— 66 kv				
Before improvement (1929–34)	6	19.3	20.6	+ 7

* Does not include a one-mile section where there are no overhead ground wires.

** It will take many more years of operation to check the accuracy of estimates for these lines because flashovers so seldom occur.

Table XII. Efficiency of Preventive Lightning Protection During Period of Study With Surge-Crest-Ammeter Links

Line	Years of Record	Lightning Outages*	Tripouts Per 100 Miles of Line Per Year	Recorded Strokes	Efficiency of Protection (Per Cent)
A—220 kv	8	0	0	696	100
F—220 kv	5	0	0	223	100
B—132 kv	8	1	0.39	354	99.7
C— 66 kv					
Before improvements	2	37	63	55	33
After improvements	5	5	3.4	198	97.5
Suspension towers		4**		143	97.2
Strain towers		4**		55	92.7
E— 66 kv (Two lines on one right of way)	4	4	1.3	236	98.3

* Single, double, and triple circuit tripouts. ** Flashovers.

the method seems to work well for shielded lines. As a further check, and to determine if the method also applies to unprotected lines, additional calculations were made for lines *C* and *D* before they were improved. The results are shown in Table XI.

Line *D*, before it was improved, is of particular interest because of its nonuniformity. The line is 22.8 miles long, and approximately 2.1 miles on each end were provided with a single overhead ground wire. There was no auxiliary grounding anywhere on the line. Detailed calculations for estimating the performance are given in the appendix, and indicate that 20.6 tripouts per year could be expected. This is an error of only seven per cent from the actual experience, as shown in Table XI.

Efficiency of Preventive Protection

The efficiency of the protection provided by the combination of insulation, grounding, and shielding can be calculated from the stroke and tripout records. Efficiency of protection is the ratio, expressed in per cent, of the number of strokes not causing tripouts to the total number of strokes. Table XII gives the record for all lines. The record of line *D* is omitted from this table, because this line is provided with a Petersen coil as well as preventive protection. It has suffered no lightning troubles during five years' operation with this double protection.

The better efficiency of protection for the suspension towers of line *C* as compared to the strain towers is probably the result of the better shielding on the former type of towers.[2] From the number of flashovers believed the result of poor shielding, the efficiency of shielding can be estimated as 99.2 per cent for the suspension towers and 96.4 per cent for the strain towers.

The 100 per cent protection experienced on the 220-kv lines during the years of surge-crest-ammeter link recording cannot be interpreted as meaning that the lines are "lightningproof." Line *A* tripped out in 1933. The grounding on the line has been improved since, but on the basis of 11 years' operation and an estimated 960 strokes, the efficiency of protection is about 99.9 per cent. This record emphasizes the need for long periods of study of well-protected lines, since flashovers occur so seldom that the strokes causing them might be considered freaks. It is evident that some further improved protection will have to be developed if perfection is to be attained.

Discharges Through Lightning Arresters

Surge-crest-ammeter brackets are mounted on 26 banks of lightning arresters and one set of 220-kv expulsion gaps. Table XIII summarizes the records obtained since the first brackets were installed in 1935. It will be noted that 35 of the 53 arrester discharges, or 66 per cent, are less than 1,000 amperes. Only three of the discharges, or six per cent, exceed 2,700 amperes.

Summary

1. The recording of lightning currents with surge-crest-ammeter links began in 1934. In the ensuing years through 1942, 5,501 bracket records have been obtained from transmission towers aggregating 13,227 tower-years of exposure. There have been 54 sets of records from lightning arresters, obtained during 155 bank-years of experience.

Table XIII. Summary of Discharges Through Lightning Arresters

Arrester Type	Period of Record in Stack- or Bank-Years	Discharge (Amperes)	Distribution of Records	
			Number	Per Cent
220-kv one-phase stacks	27	200	2	50
		2,100	1	25
		2,500	1	25
220-kv one-phase expulsion gaps	6	9,700	1	100
132-kv one-phase stacks	20	600	1	100
66-kv three-phase banks	68	1– 500	10	41.6
		501–1,000	6	25.0
		1,700–2,000	3	12.5
		2,200	1	4.2
		2,600–2,700	2	8.3
		5,400	1	4.2
		7,400	1	4.2
66-kv one-phase stacks	198	1– 500	13	54.1
		600	3	12.5
		1,050	2	8.3
		2,100–2,400	2	8.3
		2,600	2	8.3
		4,500	2	8.3
			54	

2. Based on an eight-year period of record in one case and a five-year period in the other, two similar 220-kv lines running in a generally north–south direction experienced the same average number of strokes per year within approximately seven per cent. Two similar 66-kv lines lying in an east–west direction experienced the same average number of strokes, within about three per cent, during a seven-year period.

3. A 132-kv line, having about the same span lengths as a 220-kv line but lying in a more or less northwest–southeast direction within about two miles of a river throughout its length, experienced on the average about 40 per cent more strokes per unit length than the 220-kv line during an eight-year period.

4. The maximum number of strokes recorded on one of the transmission lines in one year was 4.9 times the minimum number recorded in another year. The maximum number on this line occurred in the same year that the minimum number occurred on a comparable line.

5. The strokes in single years to a line deviated up to 89 per cent from the seven- or eight-year averages. The average number of strokes recorded in two or more consecutive years did not deviate so much as this from the long-time records. With these experience records, it is possible to estimate the long-term experience on a line from the experience of a few years.

6. The number of strokes recorded in single years on a line 92 miles long did not vary so much as those recorded on shorter lines. It may be inferred from this that the number of lightning troubles will not vary so much from year to year on long lines as it will on shorter ones.

7. The data for tower lightning currents show most records in the range from 5,000 to 10,000 amperes with a secondary maximum in the 25,000- to 30,000-ampere range. This suggests that there may be two types of lightning discharges, most of each type producing tower currents in different ranges.

8. The majority of towers were not struck more than once a year, but it is not unusual for a tower to be hit twice in one season. Only one tower during an eight-year period on two lines (5,248 tower years) has been struck three times in one year. More than three strokes per year has not been recorded on any tower under observation.

9. Of 656 towers observed for eight consecutive years, 138, or 21 per cent, did not produce records indicating that they were struck by lightning during the period. Most of the towers (78 per cent) were not struck more than twice during the eight-year period.

10. The maximum number of years in succession that any one tower has been hit is four, and this occurred only three times in eight years. Occasionally a tower is hit two years in succession, but it does not occur often.

11. It may be inferred from the extensive stroke data that the installation of localized lightning protection usually cannot be justified on the basis of a one- or two-year trouble record. The chances are that lightning will not strike the particular location in the years immediately following.

12. Three lightning currents ranging from 3,000 to 45,000 amperes have been recorded on a flagpole. The pole is about 300 feet above street level, and is the highest point within three city blocks.

13. From 104 sets of records obtained from towers with surge-crest-ammeter brackets on all four legs, it is possible to estimate the range in which a tower current lies when only one bracket record is available from a tower. When the single-leg records indicated tower currents of 2,000 amperes or less, the sum of the four leg records was as large as 50 times the single-leg indication. In the range from 2,000 to 70,000 amperes in a tower, the single-leg records usually were not very much in error. In the four cases where the tower current indicated by a single bracket was 76,000 amperes or greater, the sum of the four bracket readings was appreciably less than the single-bracket indication.

14. There were 25 cases where the surge-crest-ammeter links on a single leg yielded no current record when there was current in the other legs. These records showed tower currents as great as 6,200 amperes when no record was obtained from one of the brackets on that tower.

15. From the four bracket installations it is estimated that 6.4 per cent more strokes were recorded by them than would have been recorded if only one bracket had been installed on those towers.

16. Bonding of adjacent transmission towers appears helpful in reducing footing resistance. It apparently does not cause any appreciable change in the number of disturbances affecting more than one of a group of neighboring towers.

17. Flashovers occurred on 66-kv towers having as many as eight continuous-counterpoise conductors. However, the eight-year record of the 132-kv line indicates that a multiple-counterpoise system has prevented flashovers when the ground resistance is ten ohms or less.

18. A single continuous counterpoise seems to provide good protection for 220-kv towers having an insulator flashover level of 1,600 kv or more, provided that this type of grounding brings the footing resistances to less than the flashover value of the insulation in kilovolts divided by 100.

19. The average value of the surge impedance of a conductor in air seems to be less than 235 ohms. This is appreciably less than the theoretical value of 400 or 500 ohms often used. The data on which this estimate is based imply that mid-span clearances on transmission lines may not need to be so great as often are allowed for the prevention of mid-span flashovers.

20. Fairly good predictions of line performance can be made by a relatively simple empirical method.

21. The efficiency of protection of the 220-kv lines is estimated to be about 99.9 per cent. This high efficiency means that strokes causing flashover are so rare that many years may elapse between flashovers. The lowest efficiency of protection exists on one of the 66-kv lines, where it is about 97.5 per cent.

22. It is evident that preventive protection can be made very effective, but it does not attain perfection.

23. The largest recorded current through a lightning arrester was 7,400 amperes occurring in a 66-kv bank. A surge current of 9,700 amperes through a 220-kv expulsion protective gap was recorded.

Table XIV. Estimate of Lightning Outages on Line Section With Overhead Ground Wire

Ranges of Tower-Footing Resistance	Towers in Range		Maximum Safe Current for Towers in Range	Percentage of Tower Currents Exceeding Safe Values	Probability of Flashover
	Number	Per Cent			
0–1	3	8.83	680,000	0	0
1–25	0	0			
26–30	3	8.83	22,700	25	0.022
31–35	2	5.88	19,400	27.5	0.016
36–40	3	8.83	17,000	29	0.026
41–45	5	14.70	15,100	32	0.047
46	1	2.94	14,800	32	0.009
52	1	2.94	13,100	35	0.010
59	1	2.94	11,500	40	0.012
61–65	2	5.88	10,500	45	0.026
66	1	2.94	10,300	46	0.014
75	1	2.94	9,100	60	0.018
81–90	3	8.83	7,600	70	0.062
91–100	0	0			
101–110	2	5.88	6,200	78	0.046
114	1	2.94	6,000	80	0.024
120–125	2	5.88	5,400	82	0.048
151	1	2.94	4,500	86	0.025
305	1	2.94	2,200	95	0.028
355	1	2.94	1,900	97	0.029
Totals	34	100.00			0.46

Appendix. Estimating the Lightning Outages of a Transmission Line

Even a cursory survey of the literature on lightning protection indicates that a large number of factors should be considered theoretically when attempting to predetermine the lightning performance of a line. Some of these factors are:

1. Number of storms per 100 miles, per year
   (*a*) Number of strokes.
   (*b*) Intensity of individual strokes.
2. Topography.
3. Cloud height.
4. Effective ground plane.
5. Soil resistivity and its variations.
6. Insulation.
7. Coupling factor.
8. Span length.
9. Mid-span clearance between conductors and overhead ground wires.
10. Shielding
    (*a*) Number of overhead ground wires.
    (*b*) Location of ground wires.
    (*c*) Influence of hillsides.
    (*d*) Nearby structures.
11. Surge impedances
    (*a*) Tower.
    (*b*) Overhead ground wires.
    (*c*) Grounding system.
12. Effect of method used in measuring ground resistance.
13. Area covered by grounding system.
14. Number and type of grounding electrodes.
15. Lightning current measurements
    (*a*) Accuracy of magnetic links.
    (*b*) Distribution of current in tower.
16. Wave shape of lightning surge.
17. Movement of conductors by wind
    (*a*) Effect on shielding.
    (*b*) Effect on clearances.
18. Design features affecting electrostatic field around insulation.
19. Effect of weather conditions on electrostatic field around insulation.
20. Instantaneous value of power voltage at instant of stroke.

Since inclusion of any large number of these factors in a computation is impractical, a method[4] was developed a few years ago which included only those factors that experience had shown to be most important. Initially the method was applied only to shielded lines, but now it is extended to all lines.

Experience with line *D* before its improvement with shielding and counterpoise afforded an opportunity to test the accuracy of the computations.

Before improvement, the line had a single overhead ground wire installed on end sections only. The computation of estimated flashovers resolved itself into three parts, one for each of the three types of line sections:

1. Overhead ground-wire section.
2. Towers.
3. Spans between towers without overhead ground wires.

Detailed Calculations

1. At the two ends of the line, there was a total of 4.25 miles of single overhead ground wire. This wire is assumed to provide practically perfect shielding for purposes of calculation. There were 34 towers in the sections having shielding wire. The footing resistances of these towers were distributed as indicated in Table XIV. The line insulation had a flashover level of about 510 kv. The coupling factor between the overhead ground wire and the conductors was about 0.25. The maximum tower potential without flashover is then safe tower potential $=510/(1-0.25)=680$ kv.

In Table XIV, the maximum safe tower current is obtained for column 4 by dividing 680 kv by the maximum footing resistance in each range from column 1. From Figure 2 is entered in column 5 the percentage of tower currents that can be expected to exceed the given maximum safe value. The probability of outage per stroke for each range of tower-footing resistance is the product of the values in columns 3 and 5 expressed as decimals. The total probability of outage per stroke for the section is 0.46. Since the average strokes per 100 miles of line per year for the entire transmission system is 113 (Table II), the probable outages per year for the overhead ground-wire section is $113(4.25 \text{ miles}/100 \text{ miles})\ 0.46=2.2$.

2. The section of the line without overhead ground wires was 18.58 miles long, which experienced $(113\times 18.58)/100=21$ strokes per year. The absence of the overhead ground wire made the coupling factor equal zero and the safe tower potential equal the flashover value of the insulation, which was 510 kv.

It is assumed[5] that 20 per cent of the strokes to the line hit the towers and the remaining 80 per cent hit the line conductors. The strokes terminating on the towers are assumed to cause flashover in accordance with probability calculations similar to those made for the overhead ground-wire section. The probable flashovers per year caused by strokes to towers without overhead ground wires come to 3.3.

3. There was an average of about $0.80\times 21=16.8$ strokes each year terminating on line conductors between towers. It has been shown in the paper that the surge impedance of a conductor in air averages less than 235 ohms. On this basis, it can be assumed that the conductors on line *D* could carry more than 510 kv/235 ohms $=2{,}100$ amperes, without flashover. From Figure 2 it is roughly estimated that ten per cent of the strokes to line conductors did not cause flashover. Accordingly, about 90 per cent of the 16.8 strokes that hit the line conductors each year caused flashovers, or 15.1.

A summary of the estimated flashovers on the three types of line sections is as follows:

Towers with overhead ground wires	2.2
Towers without overhead ground wires	3.3
Spans without overhead ground wires	15.1
Estimated tripouts per year	20.6

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Supervisory Control for the World's Most Modern Steel Mill

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WARTIME EXPANSION of the Pacific Coast shipbuilding industry has accentuated the need for new steel-producing facilities in the West. Moreover, rich deposits of iron ore, coal, and limestone—the raw materials required to produce steel—are close at hand in the central inland section of the western states.

To meet this need and to take full advantage of raw materials so strategically situated, a completely integrated steel plant has been constructed in that region. In this plant, the largest west of the Mississippi River, approximately 700,000 tons of finished ship plates and 200,000 tons of structural steel will be produced annually for Pacific Coast industry. This production goal required the building of 252 coke ovens, three blast furnaces, nine open-hearth furnaces, a 45-inch slab mill, a 132-inch plate mill, and a 28-inch structural-steel mill. Supplementary units include a sintering plant for treatment of the ore and an exceptionally large central maintenance shop. Adequate outside shop facilities are not available in the region of the plant, thus necessitating self-sufficient shop facilities within the plant.

For the first time in the history of steel-mill design, the entire electric-power supply and distribution system will be under the supervision and control of a centrally located power dispatcher. Particularly significant is the fact that all apparatus in the mill motor rooms likewise will be remotely controlled and unattended.

One of the most comprehensive supervisory control systems yet constructed will permit control of all of the wide-spread units of this plant from one dispatching center. This is in distinct contrast to the previous practice of maintaining local operators at all substations and local attendants in all mill motor rooms.

Supervisory control is a selective code system designed to provide remote control and supervision of a relatively large number of apparatus units through the medium of a single pair of control wires for each remote station. This system utilizes telephone-type relays throughout, and operates from a battery source of 48 volts direct current. It is simply a means by which remote-station apparatus can be supervised and directed, just as a substation operator is directed by telephone.

General

In designing the electric-power system for a new plant, the steel-mill engineer faces the same problems of generation, transmission, and distribution that confront the central-station engineer. However, in the utilization of this power for the production and processing of steel he is in a field peculiarly his own.

Of utmost importance is the problem of supplying 700 volts direct current for the main drive motors in the slab, plate, and structural-steel mills. Large motor-generator sets are required for this purpose, together with their associated exciter sets, ventilation equipment, and miscellaneous auxiliary apparatus. Particular interest, therefore, is centered in this mill motor-room apparatus and its special control and supervision features.

Up to this time it has been customary practice to provide an attendant in each mill motor room. His function is to control locally all motor-room apparatus. His duties include switching of all primary and secondary distribution circuits for the mill, as well as starting and stopping of all motor-generator sets and associated apparatus. An idea of the labor and time that would be involved in carrying out these duties in this new mill may be gained from the fact that the plate-mill motor room is 65 by 575 feet in area.

The single-line diagram (Figure 4) indicates the magnitude of the power system, which serves a plant area approximately $2^{1}/_{2}$ miles long by one mile wide. Although not shown, a large number of radial feeders are supplied from the distribution busses indicated for each substation.

Each unit of the project is provided with an entirely independent supervisory-control setup. These units are the open hearth, sintering plant, blast furnace, shop, coke plant, 138-kv tie substation, and the slab, plate, and structural-steel mills. Although all dispatching-office equipments are located at a common point, operations can be performed, or signals received over all equipments independently.

In this application, the electrical dispatcher is also the power-station switchboard operator. The dispatching-office supervisory-control equipment for each unit of the mill is incorporated in the same cubicle which houses the direct-acting control, as well as the metering and protective relaying, for the 13.8-kv feeder circuit supplying that unit.

With the power-supply system for the mills literally at the fingertips of the dispatcher, all motor-room apparatus can be placed in service with an absolute minimum of man power and in a minimum of time. Supervisory control provides immediate notice to the dispatcher of the loss of any normal power source. Standby tie lines between substations and between motor rooms can be switched in with minimum interruption to plant production. Provision is made also for the dispatcher to control system power factor in order to maintain maximum operating efficiency.

Completely automatic control of such motor-room apparatus as motor-generator sets and mercury-arc rectifiers has been available to the industry for many years and has proved itself in service. Co-ordination of such local automatic control with remote supervisory control makes possible an operating setup based on completely unattended motor rooms. Under normal operating conditions these motor rooms will be locked, and the apparatus therein will be accessible to authorized maintenance personnel only. Likewise, all conventional substation apparatus throughout the project will be unattended and locked in a similar manner.

As in previous mill practice, operators in control pulpits overlooking the slab, plate, and structural-steel mills will control locally all operations involving the handling of steel. With all motor-room apparatus ready to serve the mill, the pulpit operator will receive automatically a visual go-ahead signal, and, through the interlocking of a permissive relay, may proceed with mill operation.

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In the dispatching office, one or more panels are assigned to each station or mill unit under remote control. The control escutcheons are physically laid out in a logical arrangement with a miniature bus system in distinctive colors to indicate all principal busses, feeders, and tie lines. Each escutcheon is a small shield or panel which has assembled on it all of the control keys, push buttons, and indicating lights associated with a given apparatus unit.

Each dispatching equipment incorporates a station-alarm lamp, a pilot or "normal" lamp, and a control-line supervision lamp. The pilot lamp is the only one of the three normally lighted. This indicates that the system is at rest but in a condition to operate. Should a fault occur the alarm bell rings, and a station lamp shows the station from which the signal is coming. A glance at the group of escutcheons locates the individual breaker or protective device which has caused the alarm through the glowing of the disagreement lamp. Thus the dispatcher is able to note quickly what has happened and to restore service by reclosing the necessary breakers.

Figure 1. A short section of the dispatching control board

In this plant, there are 11 control centers and approximately 500 control points, with provision for an additional 200 future control points. In general, all 138-kv, 13.8-kv, 6.9-kv, 2.3-kv, and 460-volt 60-cycle circuits, as well as the 250-volt d-c circuits, are controlled by the supervisory-control system. All 13.8-kv circuits in the switchhouse, coke plant, sintering plant, and slab mill are equipped with breakers of 1,000,000 kva interrupting capacity. These are of the air-blast air-operated station type. Elsewhere, feeder reactors permit the use of 500,000-kva interrupting-capacity removable-type breakers. The 250-volt d-c circuits throughout the plant are supplied from ignitron-type rectifiers with semihigh-speed heavy-duty steel-mill-type circuit interrupters.

Details of Operation

As previously noted, the supervisory-control equipment operates from 24-cell battery circuits. The use of alternating current is undesirable since the control system, which brings vital information to dispatching headquarters during times of trouble, is most important when the a-c supply is off. For each station under remote control only two telephone-type line wires are required.

The selecting system consists of straight counting codes. In case of a selection initiated by the dispatcher, the desired code is sent to the remote station, where the selected relay returns its code to the office. If the returned code does not agree with that sent out, no further operation is possible. If it does agree, the "check" or point lamp on the individual escutcheon is lighted. For a circuit-breaker operation the dispatcher then turns the twist key to the desired "close" or "trip" position and presses the master-control push button. This action sends the closing or tripping code to the selected unit, causing it to operate. As the breaker changes position, an auxiliary switch causes the new position to be reported to the dispatcher, changing the red and green lamps to correspond.

An automatic operation is reported in a similar fashion. Should a circuit breaker trip automatically, its auxiliary switch causes its selecting code to be transmitted to the office and the alarm bell to notify the dispatcher that some action is required. The office relay returns the check code to the substation, which immediately sends its position code to change the red lamp indication to green. As the dispatcher hears the bell, a glance at his row of panels informs him which substation is involved by the lighting of a station-selection lamp. On the panel reporting the alarm the individual breaker that opened is clearly indicated by the disagreement lamp. The dispatcher may select and reclose the breaker immediately. Should a permanent fault cause the breaker to reopen immediately, he may dispatch his maintenance crew to locate and correct the fault.

Once each week it may be desirable to test the over-all supervising system. The accepted method consists of a "master check," in which the dispatcher presses a master-check push button. This causes a checking code to be transmitted to the remote-control center, which compels each circuit breaker or other operated device to send its present position code to the dispatcher's board. Thus the equipment is given an over-all positive test and, at the same time, assures the dispatcher that all equipment is in good working order.

If automatic-breaker operations occur at the substation during the master-check operation they will be indicated immediately, provided that the master check has progressed beyond their point-assignment number. Otherwise, they will take their proper turn in the point-assignment sequence.

If it should become necessary for the

dispatcher to select and operate any substation device during master check, he may do so. The only special requirement is that he hold the selection key in the depressed position until the point-selection lamp is lighted.

As soon as the desired operation on that point is completed, the master-check operation will be resumed automatically. At the end of the master-check operation the alarm bell will be energized to notify the dispatcher that the check has been completed; he must then depress the "reset" key to silence the alarm.

In periodic testing or for special operations following maintenance work, it is sometimes desirable to operate a breaker several times. In order to save time and eliminate the necessity of reselection, the selection may be retained by holding the master-control push button in the closed position until the red and green indicating lamps have changed position indication. In this way the selection is maintained and, by changing the position of the twist key to the opposite position and again pressing the master-control key, the opposite operation is performed. On the last operation, if the master-control push button is not held in too long, the equipment will reset automatically and become available for any other desired operation.

The supervisory-control equipment is composed of extremely simple, mechanically uniform telephone relays, which send and receive codes at approximately 14 impulses per second. No polarized relays or selector switches are used. The control is fast, positive, and inherently antipumping, and automatically checks each selection. The entire equipment normally is at rest. Continuous individual lamp indications are provided for supervising the position of each remote apparatus unit.

Control by the dispatcher is performed in a manner very similar to that in which an operator in the remote station itself would control the apparatus by means of the usual local control. Coded operation control provides protection against the possibility of false operation of any apparatus unit due to foreign voltages on the line wires that might be caused by surges from nearby power lines or lightning.

Transfer Switches

At all remote locations any apparatus unit can be transferred at any time from remote to local control by means of local transfer switches. Remote supervision of the positions of these transfer switches is provided by voltage groups. In addition, each group of transfer switches also controls a corresponding indicating lamp installed over the substation or motor-room door. This lamp is lighted when any one of the group of transfer switches is turned to the local-control position. Thus it serves as a warning to a maintenance or test engineer about to leave the station, should he unintentionally leave one or more transfer switches in the local-control position. As a safety precaution, any apparatus unit may be tripped by local control at any time, regardless of the position of its transfer switch. The dispatcher retains supervision of positions of all apparatus units, even though they may be under local control.

Control Lines

Each pair of control lines is supervised continuously to detect open-circuit or short-circuit conditions. Each such channel is used also for telephone communication with its associated station, without interfering with supervisory-control operations. This communication feature permits the dispatcher to keep in contact with maintenance men at any station.

Should a short circuit or an open circuit develop in a control line, the alarm will sound, the "normal" lamp will be de-energized, and the line-supervision lamp will be lighted. Depressing the "reset" key will silence the alarm, but the "normal" lamp will remain de-energized and the line-supervision lamp will remain lighted until the line trouble is corrected and the "reset" key is depressed again.

If any substation-apparatus units have changed position during this time, the alarm will sound again as the "reset" key is depressed, and all changes then will be so indicated automatically and in numerical sequence.

Supervisory Versus Direct-Wire Control

Although the distances between the dispatching office and the remote-control stations are relatively short, the large number of operations required at each station fully justifies the supervisory-control system. The use of a single pair

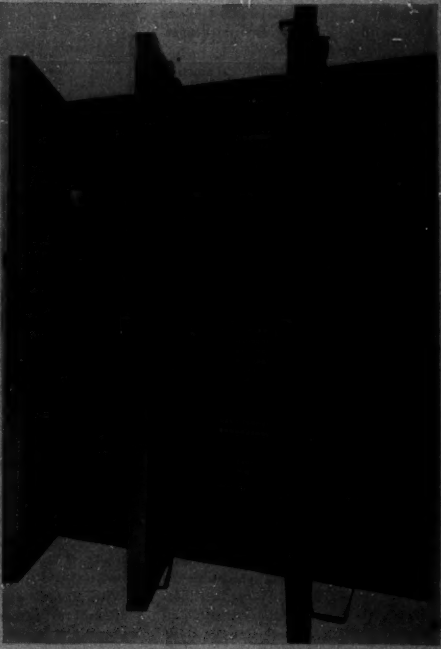

Figure 2. Dispatching-office supervisory-control cubicle, with open doors showing flush-mounted relay cases on inside panel

of wires to each remote station, as contrasted with direct-wire control, represents a major saving in copper that is so essential in today's conservation program.

The use of supervisory control has many operating advantages over any direct-wire control scheme. Supervisory control requires a minimum of space and readily accommodates future additions. Line troubles are reduced to a minimum, and the line wires are supervised continuously for open circuits and short circuits. Protection from induced voltages, or contact with power lines, is simpler to provide than with direct-wire control. Supervisory control also accommodates voice communication.

Motor-Room Control

Each mill motor-room has an oil cellar in which are located the various oil pumping systems for the bearings of the rotating apparatus within the motor room. These oil cellars are accessible from the outside of the motor room, and will be under the care of an attendant.

Because of the need for heating of the lubricating oils under certain conditions and the intricacy of the system of valves required, it was not considered feasible to place the oil-cellar equipment under supervisory control.

No action will be taken toward starting of the motor-room equipment until the oil-cellar attendant reports all oiling systems ready for operation.

Preparatory to starting the main motor-generator sets, the dispatcher will disconnect all space heaters. These heaters prevent moisture condensation on the machine windings during shutdown. Next he will start all fans and auxiliary exciter sets.

He then will start the flywheel motor-generator set, which will accelerate automatically under slip-regulator control. After the unit reaches normal speed at no load, a relay is energized which notifies the dispatcher that the set is ready for mill operation. This relay also sets up a permissive circuit to the control pulpit in the mill and lights a lamp which indicates to the mill operator that the mill is ready for his control.

The synchronous motor-generator sets in the plate and structural-steel mills also are started by the dispatcher, and will accelerate under automatic control. Reactor starting is utilized for these sets. Having reached maximum speed with the reactor in circuit, the field is applied automatically to bring the motor up to synchronous speed. At this point the starting reactor automatically is short-circuited. Through an interlock on the field switch the dispatcher is notified that the set is up to speed.

In case protective devices indicate approaching trouble, such as overheated bearings, the pulpit operator may clear his rolls of steel before the affected power-supply units are shut down.

Local interlocking is provided in each mill motor room to insure against apparatus units being started in any but the proper sequence. A synchronous or flywheel motor-generator set cannot be started until its associated exciter sets are in operation. The flywheel set, which is equipped with a wound-rotor driving motor, cannot be started unless its liquid slip regulator is in the resistance-all-in position. Furthermore, dynamic braking of this same set cannot be applied unless the regulator is again in the resistance-all-in position and the motor primary breaker is open.

Figure 3. Typical supervisory-control relay and control-escutcheon assembly

Regulator Control for the Structural-Steel Mill

The synchronous motor-generator set for the structural mill is equipped with an automatic power-factor or reactive-kilovolt-ampere regulator, which can be cut into and out of service by supervisory control. A similar regulator is provided for the two synchronous motor-generator sets in the plate mill. The dispatcher is provided with supervision of the latter regulator; that is, he has indication of whether or not it is in service, but he cannot control it.

With all mills operating normally, the plate-mill synchronous motor-generator sets will be under automatic-regulator control. The structural-steel-mill regulator will be cut out of service, and the synchronous unit will operate at a fixed full-load excitation value.

When the plate-mill regulator is cut out of service (by manual control at the regulator), then the structural-steel-mill regulator should be cut into service. If the plate-mill synchronous units are operated without regulator control, their excitation also will be set at the full-load value.

No-Alarm Feature

To complete his picture of operating conditions in the mills, the dispatcher is provided with supervision but not control of certain apparatus units directly controlled by the mill operator. This group includes such units as the scale breaker, the spreader, the edger, and the d-c breakers on the flywheel and synchronous motor-generator sets.

These units may be started and stopped, or the d-c breakers closed and opened, frequently during normal mill operation. To avoid unnecessary annoyance to the dispatcher, the associated changes in supervisory-lamp indications are not accompanied by an audible alarm, as is the case on other supervision points.

Miscellaneous Supervision Features

Continuity of power supply to all busses in all stations is supervised continuously through the medium of bus-voltage relays. This feature indicates whether or not a given bus is energized, but does not indicate the actual bus voltage. Operation of any station annunciator is reported immediately by supervisory control.

Rectifier Control

In placing a rectifier in service by supervisory control, the dispatcher must first close the a-c breaker and then the positive and negative d-c breakers. Electrical interlocking is provided to prevent the reverse sequence of operation.

Shutdown of a rectifier may be accomplished by tripping the a-c breaker, which in turn will automatically trip the d-c breakers through electrical interlocking. If desired, the d-c breakers and then the a-c breaker may be tripped independently, in that sequence.

Closing and tripping of both the positive and negative d-c breakers for a given rectifier are combined on the same supervisory control point. Supervision of the

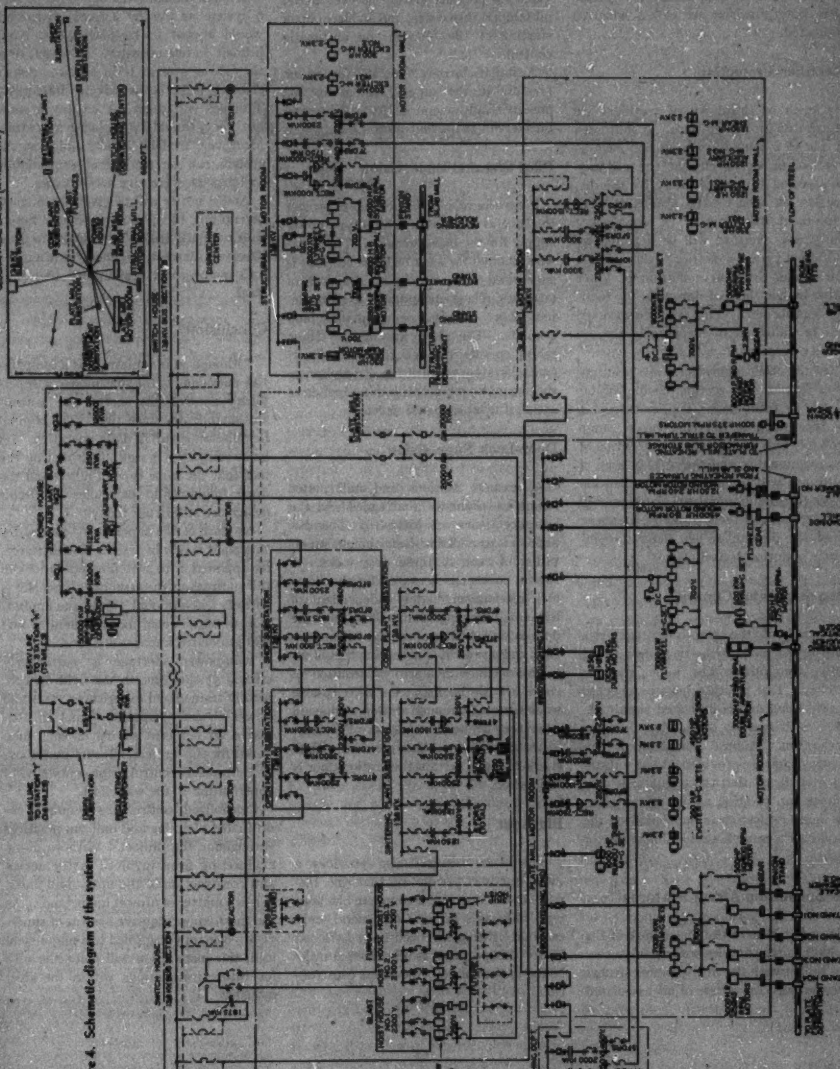
GEOGRAPHICAL LAYOUT (APPROXIMATE)
POWER HOUSE
SWITCH HOUSE 13.8KV BUS SECTION 'A'
SWITCH HOUSE 13.8KV BUS SECTION 'B'
REACTOR
DISPATCHING CENTER
STRUCTURAL MILL MOTOR ROOM 13.8 KV
SHOP SUBSTATION
COKE PLANT SUBSTATION
OPEN HEARTH SUBSTATION
SINTERING PLANT SUBSTATION
BLAST FURNACES
PLATE MILL MOTOR ROOM
FINISHING DEPT.
MOTOR ROOM WALL
FLOW OF STEEL

Figure 4. Schematic diagram of the system

position of each such pair of breakers is obtained through two interlock contacts in series (one on each breaker), to insure that both breakers are closed when so indicated.

Rectifier Protection

Causes of shutdown of rectifiers are segregated into two groups. If shutdown is due to a temporary condition such as overload or reverse current, the unit may be restored to service safely by the dispatcher as soon as the protective-relay equipment will permit. (A very heavy short-time overload will be detected by induction-type a-c overload relays, and a continuous overload condition of lower value by the thermal device on the rectifier transformer.) This type of shutdown will be indicated by tripping of the a-c and d-c breakers.

On the other hand, shutdown resulting from loss of the rectifier-cooling water supply or from excess temperature of a rectifier tank or mercury-vapor pump probably will necessitate the attention of the maintenance engineer. This type of shutdown will be indicated by operation of the rectifier annunciator alarm, as well as by tripping of the a-c and d-c breakers. Thus the dispatcher can determine his course of action accordingly.

Step-Regulator Control

Control of the step-type regulating transformer bank at the tie station to the utility company is also under the dispatcher. Tap position may be checked at any time, and is indicated simultaneously with control of the tap-changing mechanism. A local "on position" indicating lamp is provided at the substation to indicate that the regulator is on a definite tap position at any given time. The lamp indication is duplicated on the dispatcher's escutcheon for this regulator control.

Tie Substation Alarm Features

In recognition of the importance of the 40,000-kva transformer bank which serves as a tie between the utility power system and the mill, operation of all associated alarm devices is reported by supervisory control. Individual supervision is provided for pressure alarm, oil-level alarm, oil-temperature alarm, and an alarm upon starting of the blowers, by automatic control.

A similar alarm is given also for low air pressure in the supply system for the pneumatically operated 138-kv oil circuit breakers in the tie substation.

Pilot Channel Supervision

Pilot-wire relaying is utilized to protect the several sections of a loop-type power supply for the blast furnaces. The pilot-wire channel for each of these sections is supervised continuously by the type of auxiliary relays customarily used to detect short circuits, open circuits, and grounds. The alarm contacts of these relays operate through the supervisory-control system to warn the dispatcher, so that he may promptly institute corrective action if trouble should develop.

Door-Lock Supervision

All remote stations and mill motor rooms are normally unattended and the entrance doors are locked. The dispatcher is provided with continuous supervision of each of these door locks. A special contact is actuated by the door-lock mechanism, and is independent of the actual door position. Thus, once the door is unlocked, maintenance men may go in and out without actuating the alarm.

Maintenance men will be required to check in with the dispatcher by telephone when entering a remote station, and again when they are ready to leave. The door-lock supervision feature provides a final check to make certain the station door is left locked.

Blackout

Supervisory control also provides a vital wartime feature in that the dispatcher can institute a complete blackout on a moment's notice. In general, only external lighting will need to be switched off for this purpose, as all principal units of the project are of windowless construction.

Operative Burden

At first thought, the operation of such a system as this by a single dispatcher would appear to be placing too much burden on one operator. However, once the power system is in service, system changes will be relatively infrequent. Troubles will occur, as on any system, but they should be isolated in nature. Moreover, troubles at any point on the system can be recognized immediately and prompt corrective action taken.

Ample notice will be given when starting up any of the mills. Several hours' time normally is allowed in which to place in service all rotating apparatus in a given mill motor room preparatory to rolling steel.

Conclusion

Although such equipment is a comparative newcomer to the steel industry, supervisory control has served the central-station industry since 1921. The original installation is still in daily operation. Its basic soundness is well illustrated by the fact that the same type of relays and the same fundamental principles of circuit design are still in use today.

No part of the supervisory control for this plant is new in design. Furthermore, its application to switching operations in the substations throughout this project is entirely conventional. Only the application to motor-room motor-generator sets and associated apparatus is new.

Man-power shortage is particularly critical at this time in the field of technically trained and experienced steel-mill power-system operators. Thus an estimated 80 per cent saving in such man power for this project, effected by the use of centralized control, represents a vital contribution to the war effort.

Centralized control has required co-ordinated planning and uniform practices throughout the project. With so many engineering firms involved in the design and construction of the many mill units, this is a matter of utmost importance. As the most comprehensive system of supervisory control yet applied to a major steel mill, this installation will point the way for future installations within the steel industry.

Present D-C Aircraft Electric-Supply Systems

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THE multigenerator aircraft d-c electric system represents an important type which has proved its practicability under military-combat conditions. Proper parallel operation is maintained during extreme conditions of unequal generator speeds and accelerations by using an old but heretofore little-used method, which was first suggested and demonstrated for this application by L. W. Thompson. The successful parallel operation, therefore—the reliability and flexibility of the electric-supply system—is made possible chiefly by the use of voltage regulators, which provide for load division as well as voltage constancy. It was natural that difficulties initially experienced in service were not always clearly understood, and that the equipment and installation in the early stages did not have characteristics which would allow the system to operate with its greatest capabilities.[1] However, with experience and study the causes of these difficulties were determined and remedial measures taken. These changes, although only minor individually, when put into effect collectively, made for a considerable improvement in system performance. The result has been a more reliable system requiring less attention from the crew—a factor of paramount importance for a military plane. This improved reliability in turn contributes to the assignment of more functions to the electric system.

The development and refinement of the system and its control are truly a product of World War II, realized and continually improved through the co-operative effort of Army, Navy, and manufacturers' engineers. It seems appropriate that some of the problems associated with this unique type of system be discussed at this time, so that its inherent characteristics will be better understood. This paper deals with the type of system at present used by the multiengine Army type of plane. However, results can also be interpreted for the Navy type of d-c system. The emphasis of the paper is primarily upon the performance of the present system and not on developments now in progress, some of which have been or are being discussed in other papers. This paper is devoted to details of performance, the significance of which has been enhanced, not only by present military use, but by the possible adoption of a similar system for commercial aviation in the postwar period and for d-c systems more generally.

A schematic diagram of the type of aircraft electric system discussed in this paper is shown in Figure 1. An aircraft generator is connected directly to each of the four engines on the airplane. Proper parallel operation is obtained by applying a drop of 0.5 volt at full load in each of the generator negative leads to a circuit consisting of equalizer windings in the regulators. A remote-control relay which opens on the flow of reverse current provides generator switching control.

A brief summary of six of the important system problems investigated includes load division, system-voltage accuracy, reverse-current relay-contactor chatter, heavy transient loads, generator-output capacity, and short-circuit currents. Following this summary each of these problems is discussed more completely.

Load Division

Load division has been found to be affected by two major factors. These are circuit installation and regulator accuracy.

The first, which is primarily the airplane manufacturer's responsibility, requires

(*a*). That the total negative lead resistance, between the points where the equalizer-coil connections are made, be equal between generators.

(*b*). That there be no appreciable contact drop (nonlinear resistance) between points in the negative leads where the equalizer connections are made.

(*c*). That the resistance of the common lead for the field circuit and regulator coil of the regulator be small.

Voltage-regulator accuracy, that is, ability to hold voltage under all load, engine-speed, temperature, and vibration conditions, is important for proper load division. The attention given this problem by the regulator manufacturers is resulting in continued improvements in regulator accuracy.

System-Voltage Accuracy

The necessity for accurate bus voltage is determined chiefly by the battery characteristics which allow for some variation. The load voltage is determined by the voltage regulators.[2,3] It has been the practice for Army multiengine planes using up to four generators to have the equalizer coil of a disconnected generator left connected, resulting in a reduction in bus voltage. This reduction depends upon the number of generators disconnected compared to those left connected. The reduction in voltage can be avoided by providing manual means to disconnect the equalizer coils of the disconnected generators.

Another factor which has produced large variations in bus voltage has been the continued attempts of the operators to balance the loads by adjustment of the voltage regulators when improper plane circuit installation inherently would not

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allow for accurate load division for all conditions with one setting of the regulators.

Transients due to starting turrets, landing-gear motors, and so forth are appreciable and may result in instantaneous voltage dips of 30 per cent or more. Equipment essential to flight and combat should be made as insensitive to such voltage variations as is practicable.

Relay-Contactor Chatter

Relay-contactor chatter results from

(*a*). Parallel operation at light load.

(*b*). Generators with a rising-regulation characteristic (overcompounded).

(*c*). Relay contactors having reverse-current drop-out settings which are too low.

Contactor chatter with light system load probably cannot be entirely avoided. However, with improvements in the factors which affect load division, the relay contactor chatter at light load has been reduced materially.

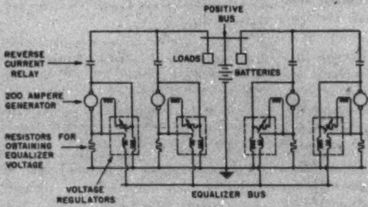

Figure 1. Schematic diagram of typical four-generator aircraft electric-supply system

A severe type of contactor chatter can be obtained from a generator which has a rising-load-regulation characteristic. Such a regulation characteristic will also tend to produce instability of voltage regulation during parallel operation. Manufacturers, in their design of aircraft generators, have been careful not to obtain a rising-voltage-regulation characteristic for any part of the speed and load range over which the generator must operate. This becomes a prime design factor and must be given proper attention both in design and production testing in order that the generators will not have such a characteristic. If the generator brushes are rotated against rotation, a generator although properly designed in this respect may have a rising characteristic such as to cause severe contactor chatter. Therefore, it is important for this reason also that the generator brushes be properly set and fixed.

Reverse-current drop-out settings of the contactors had been initially specified to be from 0 to 10 amperes with a pickup voltage of 26 to 27 volts. This inherently resulted in chatter under light-system-load conditions. The reverse-current drop-out band was increased to a band of 8 to 20 amperes reverse current with 27.5 volts on the pickup coil.

With steps made to improve circuit installation, voltage-regulator accuracy, relay-contactor accuracy, generator characteristics, and, to increase relay drop-out settings, relay chatter was greatly reduced as a serious problem.

Heavy Transient Loads

The turret motor Amplidyne or generator sets require some of the largest inrush currents of any equipment on the airplane. It is important, therefore, that the electric supply system have sufficient electrical stiffness to support the bus voltage when the turret motor sets are started. The stiffness of the electric system is increased by the number of batteries, number of generators, and high generator speeds at the time the turrets are started, while the required amount of electrical stiffness is determined by the amount of starting current required. It was found that the number of batteries greatly affected the amount of starting current that could be made available. This is particularly true if it is necessary to start the turrets with some of the available generators disconnected. Accordingly, it appears that the batteries have a very important function in helping to support the bus voltage for the shock loads incident to turret starting.

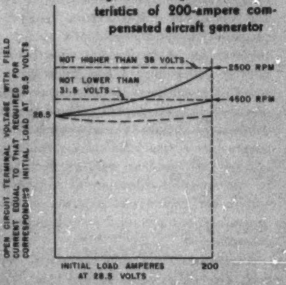

Figure 2. Regulation characteristics of 200-ampere compensated aircraft generator

Generator-Output Capacity

Two major factors affect generator-output capacity:

1. Generator design.
2. Regulator design and circuit installation.

The first is determined among other design factors primarily by whether the generator is compensated or uncompensated. The second factor is determined primarily by field-circuit resistance, which includes field leads in the airplane and the regulator minimum resistance. The output capacity of a generator may be reduced very materially by generators of uncompensated design having appreciable field-circuit resistance caused by long field leads and the use of regulators having high minimum resistance. On the other hand, the load-output capacity of a generator may be very materially improved by using generators of compensated design and regulators having low minimum-field resistance. The compensated generator was first proposed and demonstrated for this application by S. R. Bergman.[4]

Short-Circuit Currents

The modern four-engine airplane with three batteries is capable of producing total instantaneous short-circuit currents approaching 5,000 amperes. It appears, therefore, that the reverse-current contactors, which were originally specified for interruption of currents of 500 amperes and more recently improved for rated interruption of current up to 2,400 amperes, are not entirely adequate for short-circuit protection. Additional pro-

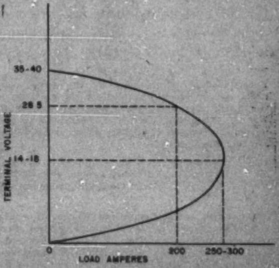

Figure 3. Volt-ampere characteristic of 200-ampere compensated aircraft generator operating without voltage regulator at 2,500 rpm

tection is desirable, since complete reliance cannot be placed on all faults to burn themselves clear[3] without appreciable damage. Accordingly, consideration is being given to proper circuit-breaker protection and contactors of higher interrupting rating.[4] Reverse-current circuit breakers provide an effective method of protecting the vital generation and battery supply currents.

Conclusions

Considerable improvement in the operation and performance of the present type of electric-supply systems for aircraft has been and can be further realized by

1. Improvements in the circuit installation.
2. Continued refinements in the generator, regulator, and reverse-current relay-contactor characteristics.
3. The use of compensated generators, regulators with low minimum resistance, and circuit installation using low-resistance field leads.
4. The use of directional protective breakers or switches in the battery and generator circuits.

Aircraft D-C Generator Characteristics

(*a*). GENERATOR-REGULATION CHARACTERISTICS

Since the generator-regulation characteristic depends to a great extent on brush setting, it is important that brushes be set correctly in order to avoid a rising characteristic which may result in instability in parallel operation and severe contactor chatter (also possible instability when connected to a battery). Rotation of the brushes in a direction opposite to machine rotation tends to produce a rising characteristic.

Figure 2 illustrates a typical regulation characteristic as determined by laboratory tests on a compensated 200-ampere aircraft generator, at 2,500 and 4,500 rpm. The ordinate is the open-circuit terminal voltage obtained with field current required for the load indicated on the abscissas at 28.5 volts. This voltage, therefore, corresponds to the machine internal voltage for the given load condition. If the curves are as shown by the full lines of Figure 2, the generator is said to have a drooping characteristic at all loads, that is, the terminal voltage drops with increase in load at constant field current. If a generator has a characteristic as shown by the dotted line, it has a rising characteristic at light load; that is, the terminal voltage rises with increase in load at constant field current. Such a generator may cause severe contactor chatter, resulting in failure of the contactor, and also cause instability in parallel operation during light loads.

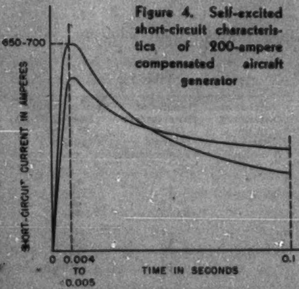

Figure 4. Self-excited short-circuit characteristics of 200-ampere compensated aircraft generator

(*b*). SELF-EXCITED GENERATOR VOLT-AMPERE CHARACTERISTIC

Figure 3 illustrates the general shape of the volt-ampere characteristic for a self-excited d-c generator operating without a voltage regulator. The field resistance is held constant at a value which gives approximately rated current at rated terminal voltage. The maximum current available depends on generator speed, excitation, brush setting, temperature, and commutation limitations.

At a generator speed of 4,500 rpm, it has been found that the volt-ampere characteristic has about the same shape and proportions as that for 2,500 rpm, when the field rheostat is set for rated load at rated terminal voltage. When a voltage regulator is used, however, the maximum current at high speed exceeds that at low speeds because of the lower excitation requirements at higher speeds.

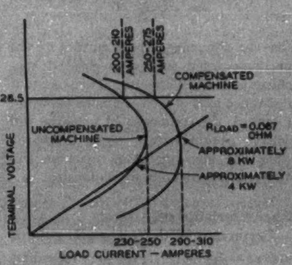

Figure 5. Comparison of volt-ampere characteristics for compensated and uncompensated aircraft generators operating at 2,500 rpm with r_{fe} = 0.25 ohm

r_{fe} = field-circuit resistance external to field

(*c*). SELF-EXCITED SHORT-CIRCUIT CHARACTERISTIC

Because of the relatively fast time constant of the armature circuit, the generator short-circuit current rises rapidly to its maximum value and decays to a sustained value at a rate determined principally by the time constant of the generator field circuit.

For a given generator speed and excitation, the maximum initial value of short-circuit current occurs for a dead short circuit at the generator terminals. This, however, does not give rise to the maximum value of sustained current. The fault resistance which will result in the largest value of sustained current may be determined from the generator volt-ampere characteristic.

A typical 200-ampere generator short-circuit characteristic as obtained with an oscillograph is shown in Figure 4. Specific values indicated on the curve are characteristic of a compensated 200-ampere aircraft generator operating at full load previous to the short circuit. If the generator is assumed to supply full load at rated voltage before the application of the fault, the initial value of the short-circuit current is practically independent of the generator speed.

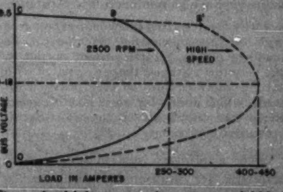

Figure 6. Volt-ampere characteristic of 200-ampere compensated aircraft generator operating with voltage regulator

The sustained value of short-circuit current depends on the fault resistance, the generator speed, and the operation of the voltage regulator. The sustained current may be increased appreciably by operation of the voltage regulator; in fact, for relatively high values of fault resistance and generator speeds, it is possible for the sustained short-circuit current to exceed the initial value.

(*d*). COMPENSATION AND ITS EFFECTS ON GENERATOR PERFORMANCE

D-c aircraft generators must have a drooping load-regulation characteristic to allow for stable operation under voltage-regulator control and to minimize relay-contactor chatter. On the other hand, a generator with a large droop will not be capable of as great an output under heavy-overload conditions. One of the most efficient ways of controlling the amount of droop is by the use of compensating windings. This has several very desirable effects.

1. Under emergency-overload conditions, the generator is capable of sustaining a much greater load than would be the case if it were uncompensated. This is of value under sudden-transient-load conditions, particularly when some of the generator capacity (on a multigenerator system) is disconnected. Also the use of compensated generators allows for the starting of higher starting-current loads and results in higher starting torque and greater acceleration of the load. This may be evaluated also in terms of obtaining better performance under emergency conditions with a reduced number of generators or batteries. Both of these are important from the standpoint of weight and reliability of operation.

2. The compensated machine allows for better commutation at higher loads and under short-circuit conditions. The proper short-circuit protection of an airplane makes it desirable that, with the occurrence of a system short circuit, none of the generators' commutators flash over.

3. A compensated machine, because of its reduced requirement for change in field current under overload conditions, is not affected so adversely as the uncompensated machine for high field-circuit resistance associated with long field leads or with regulators having high minimum-field resistance.

4. The compensated machine as compared with the uncompensated machine can be made to have a greater current output at the very low voltages, those associated with system short circuit, or very heavy overloads, which allows for more positive selectivity and simplicity of the short-circuit protective equipment.

It is a primary system-design characteristic for multiengine airplanes to provide an amount of generator compensation in order to obtain the maximum output of the generators under all system-operating conditions and at the same time to allow for proper stable parallel operation under regulator control. Both generators and regulators can be so designed to meet the optimum-system-performance characteristics desired.

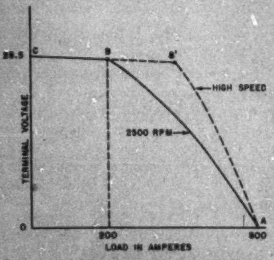

Figure 7. Volt-ampere characteristic of 200-ampere compensated aircraft generator operating with voltage regulator and battery

The general shapes of the volt-ampere characteristics for compensated and uncompensated machines of the same rating are shown in Figure 5. It will be noted that the difference in peak current for the uncompensated and compensated machines is about 60 amperes. For a constant resistance load of 0.067 ohm, as shown in Figure 5, the compensated machine will deliver approximately eight kilowatts, while the uncompensated machine will deliver approximately four kilowatts.

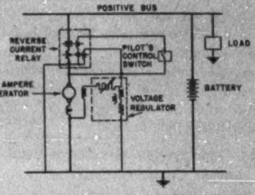

Figure 8. Connections of single-generator aircraft electric-supply system

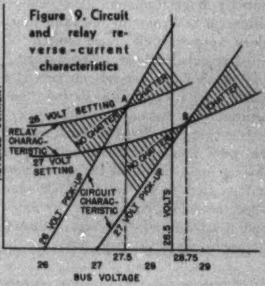

Figure 9. Circuit and relay reverse-current characteristics

Characteristics of Aircraft Generator With Voltage Regulator

Because of the wide ranges in speed and load to which an aircraft generator must be subjected and the different types and ratings of generators which may be used, a voltage regulator with a relatively large range of resistance must be employed.

The field resistance of the generator is varied by a direct-acting voltage regulator,[2,3] which controls only after the terminal voltage has reached the lower limit of the regulator setting, about 28 volts. The regulator has a magnetic circuit energized by a winding through a diluting resistor from the voltage of the generator being regulated. The force set up by the magnetic circuit is balanced against an adjustable calibrating spring. A second winding on the regulator is used to accomplish load equalization during the parallel operation of two or more generators.

When the generator is operated with a voltage regulator, the volt-ampere characteristic is similar to that shown in Figure 6. Curve *CBO* is for the base speed of the generator, 2,500 rpm. Curve *CB'O* is for a higher generator speed.

The portions of the curves *CB* and *CB'* may have a slight droop caused by resistance drop between the bus and the point at which the voltage is being regulated. At points *B* and *B'*, the voltage regulator becomes essentially inoperative, having reduced its variable resistance to a minimum value. When a compression type of regulator is used, the minimum value of regulator resistance is not absolutely constant but is a function of the generator terminal voltage. The parts of the curves *BO* and *B'O*, then, correspond to minimum field resistance. The value of load at which the regulator becomes inoperative increases with increasing generator speed because of the reduced excitation requirements.

Characteristics of a System Consisting of One Generator With Voltage Regulator, Reverse-Current Relay, and One Battery

(*a*). VOLT-AMPERE CHARACTERISTICS

When a 24-volt battery is added to the system, the volt-ampere characteristic is similar to that shown in Figure 7. At a value of load corresponding to point *B* or *B'*, depending on the speed, the voltage regulators become essentially inoperative, having reduced the variable field resistances to minimum values.

Table I

Number of Generators in the System	Number of Generators Out of Service	System Voltage Drop Due to Dead Equalizers
2	1	2.2
4	1	1.1
	2	2.2
	3	3.3
6	1	0.73
	2	1.46
	3	2.2
	4	2.93
	5	3.67

Point *A* represents the battery short-circuit current, in this case 800 amperes.

The battery tends to produce a "stiffer" system than would be obtained with a generator alone; this factor is especially important in supplying heavy shock loads such as motor starting. Since the ampere-hour rating of aircraft batteries is quite small, however, it is not intended that they be called upon to supply heavy continuous loads.

(*b*). Reverse-Current-Relay Characteristics

Generator switching control is obtained by means of an automatic relay which is set to close at a predetermined generator voltage and will open upon the flow of current from the battery to the generator.[2,3] The reverse-current relay consists of a pilot relay and contactor mounted on the same base. The calibration for pickup voltage is made by adjusting the spring tension on the pilot relay. A heavy coil mounted on the pilot relay carries the generator current and opens the relay when the flow of current reaches a preset value in a reverse direction. When the pilot relay closes, the main contactor coil is energized, and the contactor closes, connecting the generator to the bus. Figure 8 shows the connections of the generator regulator and relay and their associated circuits.

Normally the relay is set to close between 26 and 27 volts. As the generator comes up to speed with the pilot's control switch closed, the terminal voltage reaches a value equal to the pickup voltage of the relay, and the relay closes. The bus voltage, if other generators are on the bus, is normally 28.5 volts, which will cause a current to flow in a reverse direction through the relay. Hence, there is a possibility of obtaining enough reverse current to open the relay, after which it will again close because of the voltage impressed upon it. These operations continue, and the relay chatters. This condition may cause a substantial shortening of the relay life. Figure 9 shows how this condition is materially improved by raising the reverse-current drop-out value or the pickup voltage.

In Figure 9, the curves marked "relay characteristic" indicate the amount of reverse current necessary to open the relay at a given bus voltage (or relay-coil voltage) for two relay pickup adjustments. The "circuit-characteristic" curves show the amount of reverse current which the circuit will deliver at a given bus voltage when the relay pickup voltage is less than the bus voltage.

For the particular relay characteristics shown in Figure 9, the relay will not chatter when set to pick up at 26 volts so long as the bus voltage before closing is less than 27.5 volts. If the relay is set to pick up at 27 volts, it will not chatter so long as the bus voltage before closing is less than 28.75 volts.

In normal operation there is a possibility that a relay set at 26 volts will be made to close on a bus of 28.5 volts. If the relay had characteristics as shown in Figure 9, it would chatter badly if the pickup voltage were set below 27 volts. However, if the point *A* were made to be to the right of the ordinate erected at 28.5 volts, the relay would not chatter. With higher reverse-current drop-out values the intersection *A* can be moved farther to the right, and the probability for relay chatter of the type discussed can be considerably reduced. The foregoing analysis applies for conditions of very light loads. If the system is supplying some load, the probability of this type of chatter is greatly reduced, since the equalizer winding in the regulator of the incoming generator will become effective in loading the generator.

If a generator has a rising voltage characteristic (series cumulative field effect) the circuit reverse-current characteristic will be steeper, and the relay will have a greater tendency to chatter, because it takes much less voltage difference between the terminal voltage and the open-circuit bus voltage to produce a large amount of reverse current. (The intersection *A* on Figure 9 would be farther to the left of the 28.5-volt ordinate.) Relay chatter due to this cause, therefore, cannot be avoided unless the incremental generator regulation is drooping.

Another cause of relay chatter is parallel operation with very light loads on the system, aggravated by voltage-regulator inaccuracies and unequal paralleling resistors in the negative leads of the generators. Under these conditions different voltage-regulator adjustments are required to balance the generator currents at each loading. Accordingly, if the regulators are adjusted for load balance at an appreciable system load, they will be unbalanced at other loads. At light load this unbalance may result in sufficient reverse current in some generators to cause relay chatter. Proper operation requires that the paralleling resistors in the generator negative leads be equal.

(*c*). Effect of Field-Circuit Resistance

As would be expected, the maximum output of a self-excited d-c generator for aircraft is determined to a considerable extent by the field-circuit resistance external to the field itself. Figure 10 shows the effect of resistance external to the fields of a compensated 200-ampere generator and an uncompensated 200-ampere generator. Values given in this figure are indicative of orders of magnitude. These characteristics show the reduction in maximum output caused by r_{fe}, the field-circuit resistance external to the field winding. It is important, therefore, in evaluating regulator performance, to determine the minimum resistance of which the regulator may be capable when operating with a given generator.

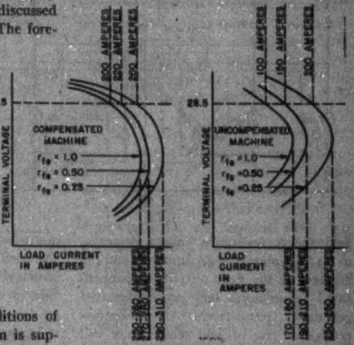

Figure 10. Effect of field-circuit resistance on compensated and uncompensated 200-ampere aircraft generators operating at 2,500 rpm

r_{fe} = field-circuit resistance external to field winding

Not only is the output of the generator reduced for higher values of field-circuit resistance, but with a 0.75-ohm increase in the field-circuit resistance, the minimum generator speed at which the regulator begins to control is increased by about 120 rpm when the regulator is set for 28.5 volts. Increasing the minimum-field resistance by 0.75 ohm increases the generator speed at which the reverse-current relay picks up by about 150 rpm with a 26-volt pickup setting of the relay. These values are representative of a system using generators having 2 to 3 ohms field resistance.

(*d*). GENERATOR COOLING

Insufficient cooling of generators will have a pronounced effect on their maximum output, from the standpoint of increased field resistance, as well as from the standpoint of increased armature resistance. When the generators are operating at full load at a low to medium speed, the regulators are inserting very little resistance into the field circuit. The field-circuit resistance is then largely the resistance of the field itself. In reference to Figure 10, it can be seen that an increase of 0.1 ohm in the field-circuit resistance causes decreases of approximately 7.5 amperes and 14 amperes in the peak outputs of compensated generators and uncompensated generators, respectively. A ten-degree-centigrade rise in field temperature will produce approximately a 0.1-ohm increase in field resistance. From the foregoing, it can be said that from the standpoint of increase in field-circuit resistance alone, the effect of temperature rise on an uncompensated machine is approximately twice that on a compensated machine. At heavy loads each ten-degree-centigrade rise in temperature will decrease the maximum output of an uncompensated machine approximately 14 amperes. For the compensated machine the decrease will be only 7.5 amperes.

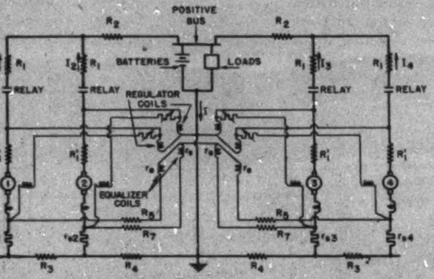

Figure 11. Connections of four-generator aircraft electric-supply system

Multigenerator Systems

(*a*). DESCRIPTION

Four generators are operated in parallel as shown in Figure 11. Each regulator is provided with an equalizer winding which is wound in a direction such as to compensate for the unbalanced load between generators. Equalizer potential is obtained by using the drop in resistors of equal value in the negative leads of the generators. Regulators are usually installed in the fuselage of the airplane where they can be readily adjusted. Relays are generally installed close to the engine-driven generators in the nacelles. Batteries connected to the main d-c bus are located in the fuselage.

(*b*). PARALLEL OPERATION AND LOAD DIVISION

1. Unbalances Due to Circuit Installation. In reference to Figure 11, an elementary sketch of a four-engine airplane electrical system assumed to be symmetrical about the load, R_1 and R_2 are positive-lead resistances; R_3 and R_4 are negative-circuit resistances set up by the bonding of the negative leads to the structure of the airplane and the structure of the airplane itself; R_5 and R_7 are equalizer-lead resistances; r_e is the resistance of the equalizer coil; r_s is the resistance of the equalizer shunts; I_1, I_2, I_3, and I_4 are the currents supplied by the generators; and I is the total load current.

If it is assumed that the identical voltage regulators have equal no-load settings, analysis of the aforementioned system indicates that load division is affected primarily by the shunt and ground resistances, r_s and R_3 respectively. If the various shunt resistances are equal, that is, if $r_{s1}=r_{s2}=r_{s3}=r_{s4}=r_s$, and if the voltage-regulator no-load settings are equal, the load division may be written approximately in terms of the following equations:

$$I_1=I_4 \tag{1}$$

$$I_2=I_3 \tag{2}$$

$$\frac{I_1}{I}\cong\frac{1}{2\left(2+\frac{R_3}{r_s}\right)}\cong\frac{I_4}{I} \tag{3}$$

From equation 3, it may be seen that if R_3 and r_s are equal, machines 1 and 4 will each supply only one sixth of the total current instead of one fourth of the total which is desired. (Machines 2 and 3 will each supply one third of the total current.) It is apparent that a decrease in the grounding resistance R_3 is necessary to improve load division. It has been found that the unbalance in the negative leads, R_3, is due to two things:

1. The resistance of the grounding structure itself (bolts, lugs, brackets, and so forth).
2. The fact that the lead resistances have not been equalized.

The airplane structure itself offers negligible resistance to the flow of current. If care is taken to make the negative leads of equal resistance and to ground the leads to a heavy portion of the airplane structure, R_3 becomes negligible ($R_3\cong 0$) and the balance under load is materially improved. If perfect load division were required, it would be necessary to balance the positive-lead resistances, R_1, the equalizer-lead resistances, and the regulator leads from the generator to the regulator coil. However, the effect on load division of unbalanced resistances in these leads is of second order. Their resistances, particularly those of the field leads, should be kept as low as possible in order to obtain maximum possible output from the generators.

2. Unbalances Due to Voltage Regulators. In addition to unbalance in load division due to the airplane circuits, considerable load unbalance can result from differences in voltage-regulator settings as small as ±0.5 volt. For example, it is obvious that an undesirable current unbalance would exist if the regulators on the *outboard* generators were set at 28 volts, while the *inboard* regulators were set at 29 volts. This difference in voltage-regulator settings may result in load unbalances as high as ±20 amperes (in systems employing an equalizer shunt voltage drop of 0.5 volt at 200 amperes) independent of unbalances due to unbalanced circuit resistances.

Variations from one regulator to another, as regards the ability of the regulator to hold voltage under all conditions of load, generator speed, and temperature will introduce additional unbalance in load division.

3. Effect of Removing Generators From Service While the Corresponding Equalizer Coils Remain Connected. It has been found by calculation and from test data that the effect of a "dead"* equalizer coil, or coils, upon *load division* is of secondary importance.

Dead equalizer coils, however, tend to reduce the system voltage. Both tests and calculations indicate that, with the present voltage regulator using an equalizer shunt voltage drop of 0.5 volt at full load, the drop in system voltage, assuming that all equalizer coils remain connected and that the generators remaining in service are fully loaded is shown in Table I.

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* An equalizer coil which remains connected to the system after its generator has been removed from service.

For systems which make use of a greater equalizer shunt voltage drop, the drop in system voltage due to dead equalizer coils will exceed the values indicated in Table I.

(*c*). Volt-Ampere Characteristic

A typical curve of bus-voltage versus load current for a four-generator system is as shown in Figure 12. This system includes voltage regulators and a battery. The portion of the curve *CB* has a slight droop with increasing load because of resistance between the bus and the points at which voltage is being regulated.

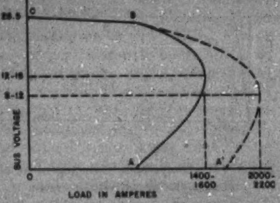

Figure 12. Volt-ampere characteristic of four-generator aircraft electric-supply system

Generators operating at 2,500 rpm

At point *B* the voltage regulators have reduced their variable resistance elements to the minimum ohmic values and are, therefore, essentially inoperative for the portion of the curve *BA*. Point *A* represents the value of short-circuit current contributed by the battery.

The effect of the battery, or batteries, is to produce a "stiffer" system, that is, to increase the maximum number of amperes available. This factor becomes important in the case of shock loads such as turret starting, and so forth. Shock loads should not be of such a resistance as to result in a collapse of system voltage. Curve *A'BC* is similar to curve *ABC*, except that the system includes three batteries rather than one. A comparison of the two curves illustrates the additional "stiffness" obtained by adding batteries to the electric system.

Table II

Number of 200-Ampere Generators	Number of 24-Volt Batteries	Approximate Maximum Sustained Fault Current in Amperes
4	3	3,400
3	3	3,000
2	3	2,500
4	2	2,800
4	1	2,400

For higher generator speeds, the maximum current available will exceed the values indicated in Figure 12 and will occur at somewhat lower voltages. The upper limits on maximum currents at high generator speeds will be governed by commutation, compensation, and saturation characteristics of the generators. It is quite possible that, with three batteries and generator speeds of 4,000 rpm, currents as high as 4,000 amperes may be obtained.

(*d*). Transient Loading

One of the most severe transients experienced on the electric system of military airplanes is caused by the starting of a turret motor Amplidyne or motor generator set. Figure 13 shows the variation in d-c bus voltage when a motor Amplidyne set is started on a four-engined airplane equipped with carbon-pile generator-voltage regulators. These transients were replotted from oscillograms taken during test flights.

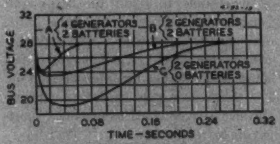

Figure 13. Bus voltage transient when motor Amplidyne set is started on an aircraft electric system

Generators operating at 3,000 rpm

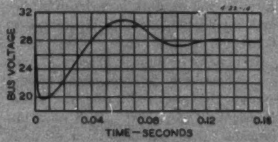

Figure 14. Bus voltage transient when landing gear is raised on four-generator aircraft electric system

Generators operating at 3,000 rpm. Three batteries on bus

The effect of the removal of generators and batteries from the supply system is clearly shown on the curves. A convenient basis upon which to compare different recovery characteristics for a constant loading condition is the area under the curves in volt-seconds. On this basis a relatively small area would correspond to a quick recovery and a small initial disturbance. For Figure 13 the following comparisons may be made:

Case	System Conditions	Volt-Seconds
A	4 generators, 2 batteries	0.12
B	2 generators, 2 batteries	0.52
C	2 generators, 0 batteries	1.21

Figure 13 and the preceding tabulation show clearly the effect of the loss of generators and batteries on the system transient characteristics.

Another severe transient load is that of the retraction of electrically operated landing gear. Figure 14 shows the variation in bus voltage when the landing gear of a four-engined plane is retracted. Actually the gear requires 20 to 30 seconds to retract fully. Figure 14 shows the initial transient, which is followed by a comparatively steady-state load of about 300 amperes of 20 to 30 seconds duration.

A 500-ampere resistance load suddenly applied to the system will cause a bus-voltage variation similar to that shown in Figure 15. Figures 14 and 15 are replots of oscillograms taken during actual flight tests on an airplane with four generators and three batteries supplying the electrical power.

(*e*). Short-Circuit Currents

Both test and analytic studies of a typical multigenerator electric-power-supply system indicate, as shown in Table II, approximate values of maximum sustained* short-circuit current as a function of system generating capacity (assuming that there is no motor load on the system):

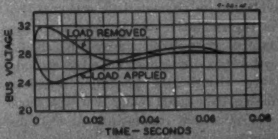

Figure 15. Bus voltage transient when 500-ampere resistance load is suddenly applied to a four-generator aircraft electric system

Generators operating at 3,000 rpm

With four 200-ampere generators and three 24-volt batteries in service, the approximate maximum value of reverse current (sustained) through any reverse-current relay is 3,000 amperes. This condition can occur when the short circuit is located between a generator and its corresponding reverse-current relay.

---

* "Sustained" short-circuit current is herein defined as that short-circuit current which can be maintained for a few seconds rather than a period of minutes.

In general, the magnitude of short circuit is dependent chiefly on the fault resistance and the number of generators and batteries in service. That is, for a given value of fault resistance and for a given number of generators and batteries in service, the short-circuit current has about the same value for a fault at any point in the supply system. The magnitude of short-circuit current also depends to some extent upon the speed of the generators. Table II was calculated on the basis of a generator speed of 3,200 rpm, assuming characteristics similar to those of a generator rated 5.7 kw, 28.5 volts, 200 amperes, 2,500/4,500 rpm, with commutating and compensating windings. Test results on this type of generator indicate that the short-time volt-ampere characteristic at 3,200 rpm is representative of the maximum sustained short-circuit current which the generators can deliver for a few seconds, even though the generator speed is higher than 3,200 rpm. Since both test and calculated results indicate that uncompensated generators produce appreciably less current at the low voltages encountered during short circuits, it is expected that the numerical results presented in Table II represent the maximum values of short-circuit current which may be expected.

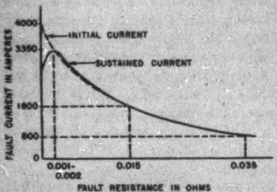

Figure 16. Fault currents on a four-generator aircraft electric system as a function of fault resistance

Three batteries in service

For a fault on a wing junction box, the fault current as a function of fault resistance is shown in Figure 16. For faults at the various generators and nacelle junction boxes, the results are much the same as shown in Figure 16.

Test and calculated data indicate that only for extremely small values of fault resistance does the initial short-circuit current exceed the sustained value. This is shown in Figure 16. At low generator speeds, however, the initial value is more likely to exceed the sustained value than at high generator speeds. The initial short-circuit current decreases with fault resistance, as shown in Figure 16, and becomes practically equal to the sustained value for fault resistances in excess of 0.001 to 0.002 ohm. The initial value is also a function of system loading prior to the fault, increasing somewhat as the system load previous to the fault is increased. In general, it is expected that the fault resistances actually encountered in practice will be of the order of magnitude to result in short-circuit currents having about the same initial and sustained values. Oscillographic records obtained on a laboratory test setup tend to confirm this conclusion. For this reason, the preceding explanation has been confined to sustained short-circuit currents.

Figure 17 illustrates the typical short-circuit-current characteristics as obtained by means of an oscillograph. The values indicated on the figure were obtained with four 200-ampere generators and three 24-volt aircraft batteries in service and correspond to fault resistances of 0.006 ohm and 0.001 ohm.

It may be seen from Figure 17 that even when the initial short-circuit current exceeds the sustained value, the initial transient is very short. It is thus improbable that the operation of most protective devices would occur before the sustained value of current was attained.

(f). Reverse-Current Protection of Generator and Battery Circuits

The short-circuit currents for faults in the generator and battery circuits may vary over a large range of values depending on the fault resistance and operating conditions prior to the fault. Also, for a given fault, the short-circuit currents contributed by the various generators are nearly equal. The difficulty, then, in obtaining adequate short-circuit protection with devices utilizing current *magnitude* only is obvious.

It is apparent, however, that for a given fault in the generator or battery circuits, the *direction* of current flow in the faulted circuit element is opposite to the normal direction of flow. Thus reverse current may be used to trip a reverse-current circuit breaker or switch to clear the fault.

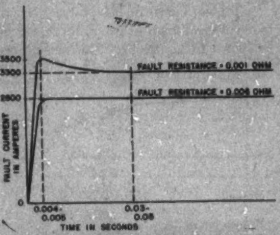

Figure 17. Short-circuit characteristic of a four-generator aircraft electric system

Three batteries in service

It is believed that a reverse-current circuit breaker is fundamental to the reliable protection of generator and battery circuits.

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Heating of Nonmagnetic Electric Conductors by Magnetic Induction—Longitudinal Flux

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MEMBER AIEE

HEAT can be generated in a piece of metal or other conducting material by wrapping a coil around the piece, such as the cylinder shown in Figure 1a, and causing a current of suitable magnitude and frequency to flow into the coil. This is called "induction heating." The great advantage of this type of heating is that the heating can be accomplished without contacting the piece to be heated. This allows the heating to take place while the piece is moving or enclosed in a protective atmosphere. Also, the heat usually can be concentrated just where it is needed, and a piece usually can be heated faster by induction than by any other method. The desirable frequency to use for induction heating depends on the size and the electrical properties of the piece to be heated and may vary from 25 or 60 cycles per second for heating joints in iron pipes to a few million cycles per second for heating very small nonmagnetic pieces for soldering. Frequencies up to 10,000 cycles per second are obtained usually from rotating machines; for higher frequencies one must resort to spark or vacuum-tube oscillators.

Induction heating finds a multitude of uses in industrial processes, such as surface hardening or heat treating, heating slugs in a continuous process for forging, heating joints for brazing, continuous heating of moving magnetic strip or wire, and the heating of small nonmagnetic terminals for soft soldering. A very successful recent application of induction heating is the use of radio-frequency power to brighten electrolytic tin plate. In this process the tin surface on the strip is melted continuously with the strip moving at 1,000 feet per minute and with an input of about 1,200 kw of 200-kilocycle power to the heating coil.

Some of the applications require the heating of magnetic material, such as iron and steel, which usually are easy to heat. Others require the heating of nonmagnetic materials, such as brass or copper, which can be heated less readily, especially when the piece to be heated is small and has a low electrical resistivity.

Since it is impractical to cover the whole field of induction heating in one paper, this paper will be limited to the heating of nonmagnetic materials.

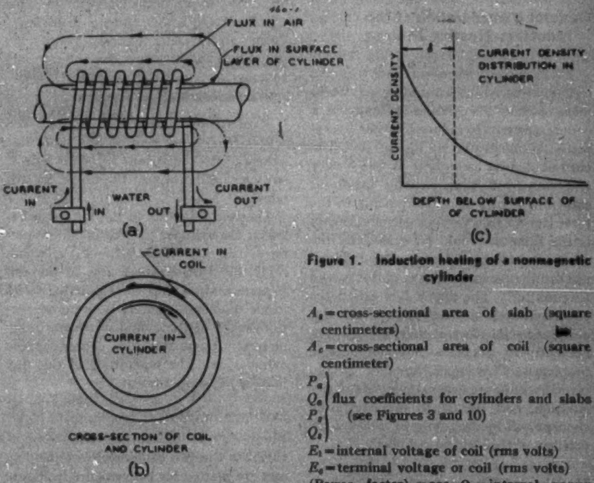
FLUX IN AIR
FLUX IN SURFACE LAYER OF CYLINDER
CURRENT IN
WATER
IN
OUT
CURRENT OUT
(a)
CURRENT IN COIL
CURRENT IN CYLINDER
CROSS-SECTION OF COIL AND CYLINDER
(b)
δ
CURRENT DENSITY
CURRENT DENSITY DISTRIBUTION IN CYLINDER
DEPTH BELOW SURFACE OF CYLINDER
(c)

Figure 1. Induction heating of a nonmagnetic cylinder

Almost any problem in induction heating can be resolved into the problem of heating a cylinder, a flat slab or strip, or a surface by a current-carrying conductor near the surface. The following discussion of these three cases, therefore, should enable one to calculate any problem where the material to be heated is nonmagnetic.

List of Symbols

Symbols will be defined throughout the paper the first time they are used, but for convenient reference all symbols are listed here:

f = frequency (cycles per second)
a = radius of a cylinder (centimeters)
d = diameter of a cylinder (centimeters)
ρ = electrical resistivity (ohm-centimeters)
$G_a(K_a a)$ = a function of $(K_a a)$ (Figure 2)
$G_s(K_s t)$ = a function of $(K_s t)$ (Figure 9)
t = thickness of slab (centimeters)
t_w = thickness of cylinder wall (centimeters)
H = peak magnetizing force at surface of piece to be heated (oersteds)
T_c = turns in heating coil
I_c = current in coil (rms amperes)
l_c = length of coil (centimeters)

$$K_a = \sqrt{\frac{8\pi^2 f 10^{-9}}{\rho}}$$

$$K_s = \sqrt{\frac{4\pi^2 f 10^{-9}}{\rho}}$$

ϕ_a = flux inside cylinder
ϕ_s = flux inside slab
A_a = cross-sectional area of cylinder (square centimeter)
A_s = cross-sectional area of slab (square centimeters)
A_c = cross-sectional area of coil (square centimeter)

$$\left.\begin{matrix} P_a \\ Q_a \\ P_s \\ Q_s \end{matrix}\right\} \text{flux coefficients for cylinders and slabs (see Figures 3 and 10)}$$

E_1 = internal voltage of coil (rms volts)
E_c = terminal voltage or coil (rms volts)
(Power factor)$_1$ = $\cos\theta_1$ = internal power factor of coil
$\cos\theta_c$ = terminal power factor of coil
W_w = total watts into piece to be heated
R = resistance of coil at the frequency used
δ = effective depth of current penetration (centimeters)
f_p = see equation 16
f_s = see equation 17
l_a = axial length of cylinder (centimeters)
l_s = length of slab (centimeters)
x = distance from center line of conductor (centimeters)
I = conductor current (rms amperes)
h_1 = distance from center of conductor to surface of plate
h = equivalent distance defined by equation 27

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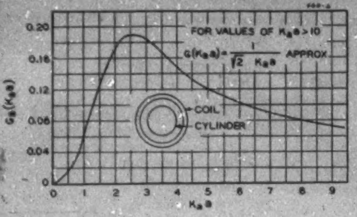
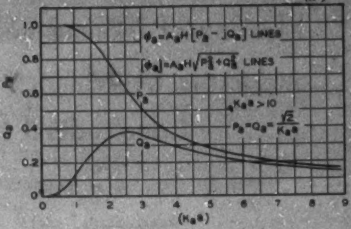


Figure 2 (left). Nonmagnetic cylinder $G_a(K_a a)$ as a function of $(K_a a)$

Figure 3 (right). Flux inside nonmagnetic cylinder



K_r = empirical resistance coefficient
R_d = d-c resistance of solid copper conductor (equation 23)
p_c = inside perimeter of coil (centimeters)
p_w = outside perimeter of work (centimeters)
l_w = axial length of work (centimeters)
ρ_c = resistivity of copper (ohm-centimeters)
ρ_w = resistivity of work (ohm-centimeters)
$W_c = I_c^2 R$ = copper loss in coil (watts)
W = watts per square centimeter or per cubic centimeter

General Consideration of the Induction-Heating Process

When current is caused to flow in the coil (heating coil) surrounding the workpiece, such as the cylinder of Figure 1a, a countercurrent is induced in the workpiece, as illustrated in Figure 1b. The current density is highest at the surface and drops off exponentially inside as shown in Figure 1c. The heating in the piece due to this varying current density is the same as would be caused by the same total integrated current distributed uniformly in a surface layer of thickness δ (Figure 1c). The depth δ is called the effective depth of penetration of current, and, in special cases, it may be used conveniently to calculate induction-heating problems. This paper, however, is concerned with more general methods of calculation, and the depth of penetration concept will be used only in treating the problem of thin-walled cylinders.

Heating of Nonmagnetic Solid Cylinders

The power generated in a nonmagnetic cylinder can be calculated from the equation[1]

$$W = 1/2H^2 fG_a(K_a a)10^{-7} \quad \text{watts per cubic centimeter} \qquad (1)$$

where

$$K_a a = a\sqrt{\frac{8\pi^2 f 10^{-9}}{\rho}}$$

f = frequency (cycles per second)
a = radius of cylinder (centimeters)
ρ = electrical resistivity of cylinder (ohm-centimeters)
$G_a(K_a a)$ = a function of $(K_a a)$ involving Bessel's functions and plotted for convenience in Figure 2
H = peak value of magnetizing force at the surface of the cylinder (oersteds)

If the magnetizing coil has closely spaced turns and a length equal to at least 20 times the spacing between the cylinder and the inside diameter of the coil, the end effect of the coil is negligible, and one may calculate H from the equation

$$H = \frac{0.4\pi T_c I_c \sqrt{2}}{l_c} \text{ oersteds} \qquad (2)$$

T_c = turns in coil
I = current in coil (rms amperes)
l_c = axial length of coil (centimeters)

If a relatively shorter coil is used, the actual H obtained will be less than that indicated by equation 2. This effect, however, will not be considered in this paper.

The magnetic flux in the air space between the cylinder and the inside diameter of the coil will be $(A_c - A_a)H$ where A_c is the cross-sectional area of the coil (square centimeters) corresponding to its inside diameter, and A_a is the cross-sectional area of the cylinder (square centimeters). Some flux also penetrates the cylinder, and the magnitude of this flux may be calculated from

$$\phi_a = A_a H(P_a - jQ_a) \qquad (3)$$

where P_a and Q_a are quantities proportional to the two components of this flux and are plotted for convenience in Figure 3. The total flux linking the coil is then the vector sum of the flux in the air $(A_c - A_a)H$ and the flux in the cylinder $A_a H(P_a - jQ_a)$. If these two are added, one obtains for the total flux

$$\phi_0 = A_a H\left[\left(\frac{A_c}{A_a} - 1 + P_a\right) - jQ_a\right] \text{ lines} \qquad (4)$$

or in actual magnitude

$$[\phi_0] = A_a H\sqrt{\left(\frac{A_c}{A_a} - 1 + P_a\right)^2 + Q_a^2} \text{ lines} \qquad (5)$$

The voltage induced by this flux in the turns of the coil will be

$$E_1 = \sqrt{2}\pi f T_c [\phi_0] 10^{-8} \text{ volts (rms)} \qquad (6)$$

From equations 1, 2, 5, and 6 one may calculate power into the work, coil current, and coil voltage for a variety of conditions. If these quantities are known, one may calculate the corresponding values of what will be termed "internal power factor." The internal power factor will be defined by the equation

$$(\text{Power factor})_1 = \cos\Theta_1 = \frac{W_w}{E_1 I_c} \qquad (7)$$

where W_w is the total power in watts put into the work. It is found that the internal power factor is represented by the equation

$$\cos\Theta_1 = \frac{2Ga(K_a a)}{\sqrt{\left(P_a + \frac{A_c}{A_a} - 1\right)^2 + Q^2}} \qquad (8)$$

Curves of internal power factor for various values of A_c/A_a are represented by the curves of Figures 4 and 5. The parameter $K_a a$ is used in equation 8 and in plotting the curves of Figures 4 and 5, so as to make them perfectly general and applicable to any frequency and any cylinder. One may calculate values of $\cos\Theta_1$ for $(K_a a)$values greater than 10 by using the relation

$$K_a a > 10 \quad P_a = Q_a = \frac{\sqrt{2}}{K_a a}$$

$$G_a(K_a a) = \frac{1}{\sqrt{2}K_a a} \qquad (9)$$

The voltage E_1 does not include the voltage drop due to the resistance of the heating coil nor to flux which actually passes through the turns (through the copper) of the coil. The coil should always be designed, so that the depth of penetration of flux and current is less than the thickness of the copper from which the coil is wound, and for this condition and for a close-wound coil the voltage drop due to flux inside the copper will be approximately equal to the resistance drop of the coil. The volt-amperes consumed in the copper of the coil on this assumption will be $I_c^2 R(1+j)$ or will have the absolute value $\sqrt{2}I_c^2 R$ where R is the resistance of the coil at the frequency used. The total volt-amperes at the terminals of the coil may be obtained by adding the volt-amperes in the copper of the coil to the volt-amperes E_1I_c, as shown in Figure 6a. This diagram is drawn for the case where the value of volt-amperes in the copper of the coil $\sqrt{2}RI_c^2$ is small compared with E_1I_c. The actual volt-amperes input to the coil is, in this case, only slightly greater than the internal volt-amperes E_1I_c, and the power factor at the coil terminals $\cos \Theta_c$ differs only slightly from the internal power-factor $\cos \Theta_1$.

One may calculate the total volt-amperes in the coil from the internal volt-amperes E_1I_c and the relation

$$E_cI_c = \sqrt{(E_1I_c \cos \Theta_1 + I_c^2R)^2 + (E_1I_c \sin \Theta_1 + I_c^2R)^2} \text{ volt-amperes} \quad (10)$$

where

$$\cos \Theta_1 = \frac{W_w}{E_1I_c}$$

Figure 6b represents approximately the conditions which would be obtained in heating a copper cylinder with the heating coil wound closely around the copper cylinder and with the frequency high enough to make the internal power factor equal to 0.707. This is not a condition likely to be obtained in practice but is included to show that, in an extreme case, the volt-amperes in the copper of the coil may be equal to half the total volt-amperes supplied at the terminals of the coil.

The actual power factor at the terminals of the coil is

$$\cos \Theta_c = \frac{W_w + I_c^2R}{E_cI_c} \quad (11)$$

The curves of Figures 4 and 5 are valid only when the cylinder to be heated is equal in length or longer than the heating coil. The internal power factor for the case where the coil is slightly longer than the cylinder, as often will be the case, may be estimated by reducing the values given by these curves in the ratio l_s/l_c where l_s is the length of the cylinder in centimeters.

It is, in general, desirable to operate at a frequency equal to or greater than that which will give the maximum internal power factor. From the curves of Figures 4 and 5 this corresponds to a value of $K_sa \geq 3$. Solution for the corresponding frequency indicates that the frequency to use is

$$f \geq \frac{114\,\rho 10^6}{a^2} \text{ cycles per second} \quad (12)$$

In some cases, where a very large cylinder of low resistivity is to be heated, this equation may indicate a minimum frequency which is absurdly low from the standpoint of the generator. In such cases, a higher frequency should be used. Also, since a low frequency requires a thick conductor in the heating coil, the mechanical considerations of coil design may force one to use a higher frequency than that which would give a maximum internal power factor. There will be other cases where one wishes to heat only a very thin layer on the surface of the cylinder. In this instance, the frequency should be chosen high enough to make the effective depth of penetration of current defined by the equation

$$\delta = 5{,}030\sqrt{\frac{\rho}{f}} \text{ centimeters} \quad (13)$$

equal to or less than the depth to which heating is desired.

Heating of Hollow Nonmagnetic Cylinders

If one desires to heat a hollow nonmagnetic cylinder having a wall thickness equal to or greater than about one-tenth its diameter, the frequency should be chosen to make δ (equation 13) equal to or less than the wall thickness and use the method of calculation for solid cylinders. The only precaution necessary is to remember that equation 1 gives the average watts per cubic centimeter in a solid cylinder, and, since the total power input to the hollow and solid cylinders will be equal when δ is equal to or less than the wall thickness (t) of the hollow cylinder, the watts per cubic centimeter in the hollow cylinder will be greater than that indicated by equation 1 in the ratio of the cross-sectional area of the solid cylinder to that of the hollow cylinder.

Heating of Thin-Walled Hollow Cylinders

When the wall thickness of a cylinder becomes less than about one-tenth the diameter, one can obtain satisfactory heating with a frequency less than that which makes δ equal to the wall thickness. When the depth of penetration is several times the wall thickness, the cylinder may be considered as a short-circuited secondary of an air-cored transformer with uniform current density throughout the thickness of the wall. For this condition it can be shown that the power input to the cylinder is

$$W = \frac{1.23 f^2 d^3 t_w \rho H^2 10^{-16}}{\rho^2 + 3.87 t_w^2 f^2 d^2 10^{-16}} \quad (14)$$

watts per square centimeter

d = outside diameter of cylinder (centimeters)

This equation is applied to a thin-walled brass cylinder with the results plotted in Figure 7 (curve B). It is seen that the power input increases rapidly at first, as the frequency is increased, and then becomes constant. The vertical dotted lines indicate the frequency f_p where the input has reached 90 per cent of the value it has in the flat part of the curve and f_δ, the frequency at which the depth of penetration δ becomes equal to the wall thickness. Equation 14, of course, is no longer valid when δ approaches or gets less than the wall thickness, but in this region one may use equation 1 with the approximation $G_s(K_sa) =$

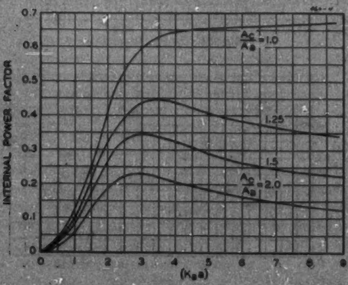


Figure 4. Internal power factor for nonmagnetic cylinder

Cylinder longer than coil

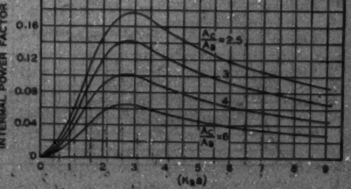


Figure 5 (right). Internal power factor for nonmagnetic cylinder

Cylinder longer than coil

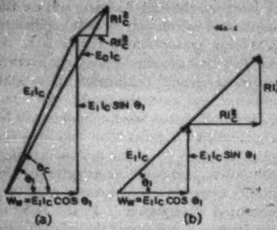


Figure 6. Vector diagrams of volt-amperes in coil

(a). Heating high-resistance material
(b). Heating low-resistance material

$1/\sqrt{2}K_a a$. When rewritten, this equation becomes

$$W = 0.63 \times 10^{-4} H^2 \sqrt{\rho f} \quad \text{watts per square centimeter} \quad (15)$$

Calculation of the hollow brass cylinder with this equation gives curve *A* (Figure 7). This equation is not valid when δ is greater than the wall thickness of the cylinder but agrees well with equation 14 when $\delta = t_w$. The two curves probably can be joined with the dotted curve shown. It seems, therefore, that one can, with little error, calculate with equation 14 below f_δ and with equation 15 above f_δ. The two frequencies f_p and f_δ will come closer together when the ratio of diameter so wall thickness is decreased (Figure 8). These frequencies may be expressed as

$$f_p = \frac{153\rho 10^6}{t_w d} \quad (16)$$

and

$$f_\delta = \frac{25.6\rho 10^6}{t_w^2} \quad (17)$$

The ratio of these frequencies is

$$\frac{f_\delta}{f_p} = 0.167 \frac{d}{t_w}$$

This shows that the two frequencies coincide approximately when $d/t_w = 6$ and are farther apart the greater the ratio d/t_w. If satisfactory power input can be obtained at the frequency (f_δ), one may as well operate at the lower frequency (f_p) or at some intermediate frequency. Only in cases of extreme power input should it be necessary to go to a frequency greater than (f_δ).

Heating of Nonmagnetic Slabs or Strip

A flat nonmagnetic slab, thin in comparison with its width (see cross-section view Figure 9), can be heated by induction, and the power input is given by[3]

$$W = 1/2 H^2 f G_s(K_s t) 10^{-7} \quad \text{watts per cubic centimeter} \quad (18)$$

where

$$K_s t = t \sqrt{\frac{4\pi^2 f 10^{-9}}{\rho}}$$

t = thickness of slab (centimeters)
$G_s(K_s t)$ = a function of $K_s t$ given in Figure 9

As in the case of the cylinder, the magnetizing force may be calculated from equation 2, and the internal voltage E_1 may be calculated from equation 6. The total flux inside the coil for this case will be

$$\phi_0 = A_s H \left[\left(\frac{A_c}{A_s} - 1 + P_s \right)^2 - jQ_s^2 \right] \text{lines} \quad (19)$$

or in actual magnitude

$$[\phi_0] = A_s H \sqrt{\left(\frac{A_c}{A_s} - 1 + P_s \right)^2 + Q_s^2} \text{ lines} \quad (20)$$

where

A_s = cross-sectional area of slab (square centimeters)
A_c = inner cross-sectional area of coil (square centimeters)

P_s and Q_s are given by the curves of Figure 10.

The internal power factor will be

$$(\text{Power factor})_1 = \cos \Theta_1 = \frac{2G_s(K_s t)}{\sqrt{\left(P_s + \frac{A_c}{A_s} - 1 \right)^2 + Q_s^2}} \quad (21)$$

Values of internal power factor for various values of the ratio A_c/A_s are plotted in Figures 11 and 12. If values taken from these curves are used, the total volt-amperes in the coil and the true power factor at the terminals of the coil may be calculated from equations 10 and 11, respectively.

As in the case of the cylinder, the curves of Figures 11 and 12 are good only if the slab is equal in length or longer than the coil. If the length of the slab is only a little less than the length of the coil, the internal power factor may be obtained by

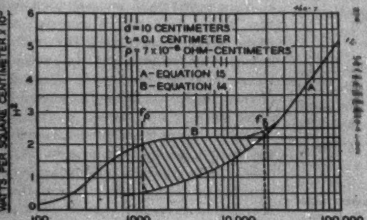


Figure 7 (left). Power input to hollow brass cylinder

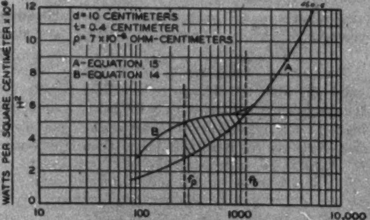


Figure 8 (right). Power input to hollow brass cylinder

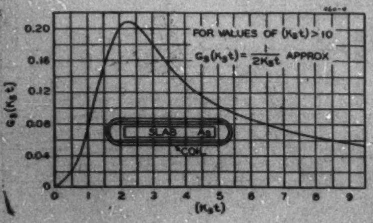


Figure 9 (left). Nonmagnetic slab or strip $G_s(K_s t)$ as a function of $(K_s t)$

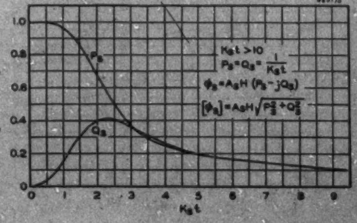


Figure 10 (right). Flux inside nonmagnetic slab or strip

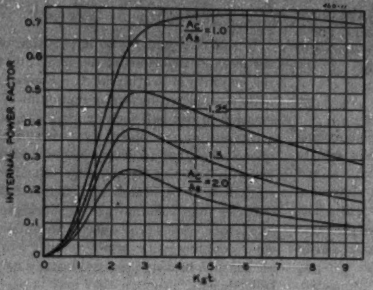


Figure 11 (left). Internal power factor, nonmagnetic slab or strip

Slab or strip longer than coil

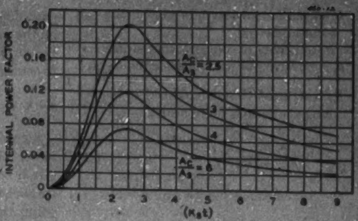


Figure 12 (right). Internal power factor, nonmagnetic slab or strip

Slab or strip longer than coil

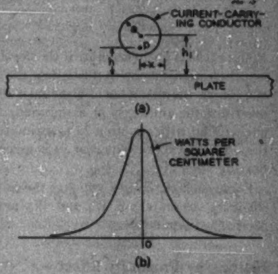


Figure 13. Heating surface of plate by proximity effect

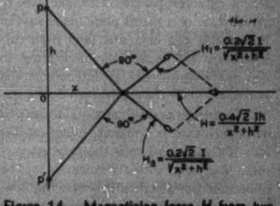

460-14
p
h
x
0
90°
90°
$H_1 = \frac{0.2\sqrt{2}\,I}{\sqrt{x^2+h^2}}$
$H = \frac{0.4\sqrt{2}\,Ih}{x^2+h^2}$
$H_2 = \frac{0.2\sqrt{2}\,I}{\sqrt{x^2+h^2}}$
p′

Figure 14. Magnetizing force *H* from two line conductors

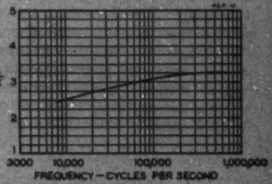


Figure 15. K_r versus frequency for use with equation 23

reducing the value given by the curves in the ratio l_s/l_c, where l_s is the length of the slab in centimeters.

Since the internal power-factor curves of Figures 11 and 12 peak around 2.5 in the practical range of the ratio A_c/A_s, it can be agreed that the frequency to be used in heating a nonmagnetic slab is determined by the relation $K_s t \geq 2.5$ or

$$f \geq \frac{158\,\rho\,10^8}{t^2} \text{ cycles per second} \tag{22}$$

Heating Cylinders and Slabs of Small Diameter or Thickness

The analysis given previously may be applied to small nonmagnetic wires and thin nonmagnetic sheet, but, because it is difficult to construct a coil to conform closely to the surface of the work in these cases, it is found that the power factor of the heating coil and the efficiency of the process are extremely low. Small-diameter iron wire and thin iron sheet, on the contrary, may be heated quite efficiently, because the magnetic permeability acts to increase the skin effect greatly.

Approximate Calculation of I²R Loss in Coil—Coil Efficiency

Some work has been done to develop formulas to predict the high-frequency resistance of coils of simple shapes, but these equations are generally tedious to use and are useless for irregularly shaped coils. The writer has found it convenient in the case of coils wound from hollow copper tubing to calculate the high-frequency resistance of the length of tubing used in a coil, as if it were a straight isolated conductor, and then multiply this value of resistance by a factor K_r greater than unity to take into account the fact that the tubing is wound into a coil.

In accordance with the aforementioned material, one may write for the resistance at the frequency (f) of a coil wound from hollow copper tubing[1]

$$R = K_r R_0(0.038d\sqrt{f} + 0.25 \text{ ohms}) \tag{23}$$

K_r = empirical[1] resistance factor

d = outside diameter of tubing (centimeters)

R_0 = d-c resistance of a solid round copper conductor having the same outside diameter and same length as the tubing used in the coil

f = frequency in cycles per second

Tests indicate that the high-frequency resistance of single-layer coils of circular cross-section wound from one-fourth-inch outside diameter to one-half-inch outside diameter copper tubing with diameters from two to four inches and lengths equal to or greater than their diameters may be calculated fairly accurately from equation 23 by using values of K_r given in Figure 15. Some tests made on coils of rectangular or oblong cross section (width more than five times the thickness) showed the resistance of this type of coil to be less frequency sensitive than the coils of circular cross section. The high-frequency resistance of this type of coil may be calculated approximately from equation 23, using the value $K_r = 2.5$.

It must be noted that, for equation 23 to be valid and also to obtain an efficient coil design, the wall thickness of the copper tubing used should be greater than the depth of current penetration, equation 13, at the frequency used. Above ten kilocycles, standard annealed copper tubing with an outside diameter of from one-fourth to one-half inch is satisfactory.

Another method of approximating the copper loss in the coil is to consider the fact that, when the frequency is high enough for satisfactory heating $K_s a \geq 3$ for cylinders and $K_s t \geq 2.5$ for flat plates, the current in the work and the ampere turns in the coil per centimeter length are approximately equal. If the coil were close wound of copper strap or a single cylindrical turn of copper, the ratio of loss in the coil to power in the work (nonmagnetic) should be approximately

$$\frac{W_c}{W_w} = \frac{p_c}{p_w} \cdot \frac{l_c}{l_w} \sqrt{\frac{\rho_c}{\rho_w}} \tag{24}$$

p_c = inside perimeter of coil (centimeters)

p_w = outside perimeter of work (centimeters)

l_c = axial length of coil (centimeters)

l_w = axial heated length of work inside coil (centimeters)
ρ_c = resistivity of copper (ohm-centimeters)
ρ_w = resistivity of work (ohm-centimeters)

For the usual close-wound coil of copper tubing the ratio is more nearly

$$\frac{W_c}{W_w}=1.3\,\frac{p_c}{p_w}\,\frac{l_c}{l_w}\sqrt{\frac{\rho_c}{\rho_w}} \tag{25}$$

The coil efficiency can be expressed as

$$\text{Coil efficiency}=\frac{1}{1+\dfrac{W_c}{W_w}} \tag{26}$$

Equation 25 is fairly accurate if the depth of penetration (equation 13) is less than one-fourth the diameter of a cylinder or one-third the thickness of a slab and is quite useful with equation 26 to estimate the efficiency to be expected in a given heating job.

Equation 26, of course, will give the coil efficiency accurately if W_c and W_w are determined accurately.

Heating by Proximity Effect—Flat Nonmagnetic Plate

If a conductor carrying alternating current is arranged parallel with and close to the surface of a metal plate, as shown in Figure 13a, the magnetic field from this conductor will cause countercurrents to flow in the surface of the plate next to the conductor and to be concentrated approximately under the conductor. If the depth of penetration of current, equation 13, into the plate and into the conductor is small compared with the thickness of the plate and the distance between the conductor and the plate, the problem becomes very similar to the classical electrostatic problem of a charged cylinder near a metal plate.[3,4] The magnetizing force at the surface of the plate is the same as would be caused by two line conductors carrying current in opposite directions, one at (p) height (h) above the surface of the plate and one at (p') distance (h) below the surface. The value of (h) is determined from the height of the center of the conductor (h_1 centimeters) above the surface and the radius of the conductor (a centimeters) and the relation

$$h=\sqrt{h_1^2-a^2}\ \text{centimeters} \tag{27}$$

Figure 14 shows how the magnetizing forces of these two conductors add to give a resulting magnetizing force at and parallel to the surface of the plate at a point distance (x) from the origin. The value of this magnetizing force (peak) is

$$H=0.4\sqrt{2}I\frac{h}{x^2+h^2}\ \text{oersteds} \tag{28}$$

I = conductor current (rms amperes)

The heating in a surface for a given value of H parallel to the surface may be calculated from equation 15. If the value of H from equation 28 is substituted in equation 15, the density of heating at the surface of the plate is

$$W=0.202\times10^{-4}I^2\sqrt{\rho_f}\left(\frac{h}{x^2+h^2}\right)^2\ \text{watts per square centimeter} \tag{29}$$

The curve of Figure 13b shows the power input to the surface calculated from equation 29 for the size of conductor and spacing shown in Figure 13a.

By integrating equation 29 from $x=0$ to infinity in both directions, it is found that the total power input to the plate by a conductor (l) centimeters long is

$$W=0.316\times10^{-4}\frac{lI^2\sqrt{\rho_f}}{h}\ \text{watts} \tag{30}$$

Equations 29 and 30 may be applied also to a single-turn circular coil around a cylinder, provided that the turn conforms closely to the surface of the cylinder. In this case, the value of (l) to use in equation 30 is the circumference of the cylinder. These equations break down in any case when (h) approaches zero because of the approximate assumptions on which they are based. They cannot be applied accurately if $h<10\delta$ where δ is calculated for the workpiece from equation 13.

Conclusions

It will be found by an application of the methods described in this paper, that the majority of heating jobs on nonmagnetic workpieces having a minimum dimension of one-half inch or more can be accomplished satisfactorily with frequencies less than 10,000 cycles per second (rotating machine frequencies). Smaller sections may be heated with higher frequencies from vacuum-tube or spark-gap oscillators. Since it is difficult to obtain close coupling with small nonmagnetic pieces, such heating processes usually will be inefficient, although they may be feasible economically.

Hollow cylinders with thin walls often may be heated with frequencies much lower than the frequency required to heat a flat sheet equal in thickness to the wall thickness of the cylinder, as the cylinder acts as a short-circuited secondary winding.

The concept and method of proximity heating is very useful in applying induction heating locally to heat the workpiece. One should remember that, when the frequency is high enough, the currents in the inducing coil or conductor cause image currents to flow in and heat the workpiece.

When it is desired to heat a piece throughout, the minimum frequency indicated by equations 12 and 22 should generally be used; however, if a superficial heating of the surface is desired, a higher frequency should be used. In the latter case, the power input also must be high enough to establish the high temperature gradient necessary at the surface.

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Basic Considerations in Selection of Electric Systems for Large Aircraft

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Synopsis: This paper presents some of the considerations involved in selecting electric systems for large aircraft. Such systems are undergoing intensive development now; consequently it is not possible to give complete answers to all problems which arise. Also, certain parts of the electric systems are subject to military classification and cannot be discussed. Nevertheless it is felt that many engineers, particularly those engaged in the design of such systems, will welcome a brief review of the basic factors involved.

Industrial and central-station experience provide a valuable background for study of aircraft systems. However, too close an adherence to previous practice should not be maintained, since aircraft systems differ in many ways from those of land equipment. The need for minimum weight demands that only those features be included which fundamental considerations show to be necessary or which at least provide benefits that justify the weight involved.

ELECTRIC systems on aircraft have been undergoing continuous evolution since the airplane was invented. Early planes started with a six-volt d-c system patterned after the automobile. Increasing power requirements led, first, to the adoption of 12-volt systems and, later, to the 27-volt* d-c systems which are now in common use. A few airplanes have been operated with 120-volt d-c systems, and some with a-c systems having various frequencies ranging from 60 to 2,400 cycles per second.

The 27-volt d-c electric system for aircraft has done an excellent job to date. It is simple, light, easy to control, reliable, and the electric-shock hazard to personnel is negligible. Therefore, it is proper to ask, "Why not use this system for all planes, especially since wartime conditions already are straining our engineering and production facilities severely?" The answer to this question lies in the fact that while the 27-volt system has proved satisfactory on the larger airplanes such as the *B*17 Flying Fortresses and the *B*24 Liberators, which are now performing in an excellent manner on our fighting fronts, there are new designs on the drafting boards to meet new military requirements. The size, performance, and electric load requirements of these proposed new planes are such that the 27-volt d-c system is no longer adequate or efficient from the standpoint of weight, and consideration must be given to new types of systems for these larger planes.

Since airplanes are designed for many different functions, it is not likely that a single type of system will be the best for every case, even among large planes. Thus, military-plane requirements differ from those of commercial planes. Small plane requirements differ from those of large planes. Altitude, range, nature of mission, and many other factors which are discussed in this paper, affect the choice of an electric system. It is believed that a review of the principal considerations involved will be of value in selecting a system for any given case.

Although the power system is always tailored to the airplane, there are some variables, such as operating voltage and frequency, which can be standardized at certain levels and thus provide the industry with the benefits of standardization. The United States Army Air Forces are contributing to electrical progress in this respect by their standardizing and co-ordinating work. The selection of 400 cycles for a-c systems for certain large military planes is a recent example.[1] A considerable part of this paper deals with problems encountered in such systems.

Purpose and Requirements of Electric System

The purpose of an electric-power system on an airplane is to generate and to distribute electric power to the various pieces of utilization equipment which are required on the airplane. The best system for any particular application is the one which combines most effectively the following features:

1. Maximum reliability under all conditions encountered in operation
(*a*). Reliability of equipment such as motors and generators.
(*b*). Service continuity should be maintained automatically even during faults or emergency conditions, insofar as possible.
(*c*). Minimize hazard to operating personnel, airplane, and equipment.

2. Minimum weight
(*a*). Minimum installed weight.
(*b*). Maximum efficiency (fuel economy).
(*c*). Minimum additional aerodynamic drag for cooling requirements (fuel economy).

3. Minimum space requirements.

4. Simplicity
(*a*). Ease of operation.
(*b*). Minimum training time for operating and maintenance crew.
(*c*). Ease of maintenance in field.
(*d*). Rapid and accurate checking in field.
(*e*). Minimize spare-part requirements.
(*f*). Ease of installation
(1). Initially.
(2). Replacement parts.

5. System should not interfere with the performance of the airplane or its equipment in any way, or at least as little as possible.

6. Satisfactory life of all component parts
(*a*). Minimum frequency of replacement.
(*b*). Minimum frequency of overhaul.
(*c*). Minimum frequency of maintenance.

7. Least cost in man-hours and dollars.

8. Utilize existing designs of aircraft accessory equipment as far as is practical.

The desirability of these features is self-evident and, therefore, need not be discussed in detail. However, it might be well to point out that certain factors are interrelated. For example, fuel consumption can be translated in terms of weight for a given-length flight. Ease of operation and maintenance are associated with service reliability et cetera. In diverse applications the various factors have different degrees of importance, and it is necessary to compromise on a system which provides the best over-all characteristics for the particular application under consideration.

The evaluation of weight and efficiency of aircraft accessory equipment is particularly important in long-range aircraft, since fuel must be provided not only to operate it but also to carry the fixed weight of the equipment plus any additional structural-weight requirements introduced by it and the necessary added fuel. Any increase in fixed weight tends

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* Sometimes referred to as 24-volt or 30-volt systems. Equipment for such systems is usually designed as follows: generators, 30 volts; motors, 27 volts; relay and contactor coils, 18–29 volts. Utilization-equipment nameplates are marked 24 volts. Voltage regulators are adjusted to hold 28.5 volts at the bus.

to decrease the pay load per pound of fuel for a given airplane and range. For example, for a 20-hour cruising range, approximately one-half pound of fuel is required to carry one pound of fixed weight over this range.[1] In addition, the engine fuel rate is approximately one-half pound of fuel per horsepower-hour. On the basis of an assumed over-all system efficiency of 50 per cent (which includes generator losses, motor losses, and distribution losses), this means that 20 pounds of fuel are required to obtain an electric-motor output of one horsepower continuously during a 20-hour flight. A gain of ten per cent in the over-all efficiency of the system thus would result in a saving of $3^{1}/_{3}$ pounds of fuel per horsepower of continuous motor load. Thus, for this case, if the increase in over-all system operating efficiency of ten per cent can be made for an increase of system fixed weight of less than $3^{1}/_{3}$ pounds per motor horsepower, a net gain can be shown. If, however, the increased weight exceeds the weight of the fuel-saving possible, the higher-efficiency equipment is not justified.

Operating Conditions

For satisfactory results, apparatus used in aircraft systems should be designed to withstand the entire range of conditions encountered in use. Many of these conditions are encountered in industrial designs, but the combination of conditions, together with the weight and space requirements, is practically unique to airborne equipment. These conditions are as follows:

1. Ambient-temperature range.
2. Altitude range.
3. Exposure to dust, sand, salt, fungus, humidity, and corrosion.
4. Vibration.
5. Flight-attitude range.
6. Acceleration.

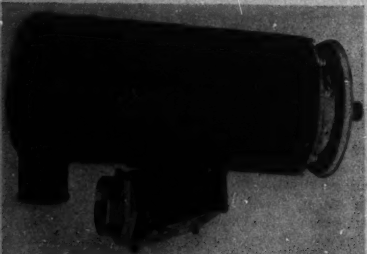

Figure 1. Side view of main-engine-mounted air-blast-cooled d-c aircraft generator

4,550/8,000 rpm; 30 volts; 300 amperes, continuous; 450 amperes, two minutes

Weight, 48 pounds; diameter, six inches; length, 14 inches

Numerical values can be assigned to these quantities only for specific airplanes. These values should be prescribed by the airplane designing engineer to meet his particular requirements.

For military planes, in addition to these requirements, it is desirable to design a system which has the minimum exposure and vulnerability to damage resulting from enemy gunfire. This means that damage in one part of the system should not be communicated to other parts of the system.

Driving Power for Generators

Some possible sources of driving power for aircraft generators are as follows:

1. Accessory drive shaft from main engine.
   (*a*). Bolted directly to a pad on the main engine.
   (*b*). Separately mounted and driven by a flexible shaft from main engine.
2. Auxiliary gas engines or gas turbines.
3. Exhaust-gas turbines.
4. Steam turbine using exhaust heat.
5. Wind-driven generators.

When a generator is bolted directly to the main engine on a suitable accessory power take-off pad, certain limitations are encountered which must be met by the generator design to enable it to fit into the space available without putting too great a strain on the engine housing. Thus the diameter, length, and bending moment (at the generator flange) are limited. Also, the generator must be designed to withstand the relatively severe vibration encountered when it is mounted directly on the engine.

These limitations dictate the maximum physical size and weight of the generator that can be mounted on a given engine.[12]

By mounting the generator separately and providing a flexible-shaft drive from the main-engine take-off pad to it, these limitations are removed, but the additional weight and space requirements of the separate mounting and flexible shaft must be considered.

Accessory-shaft drive from the main engine, with a generator bolted directly to a take-off pad on it, is probably the most common form of drive today. This is because it provides an adequate, reliable source of power for little added weight and because it operates with good fuel economy. In a multiengine plane in which several generators are used they may be operated either isolated or in parallel. D-c generators with suitable characteristics and voltage regulators may be paralleled even though the several engines may be running at different speeds. If constant-frequency a-c generators are used, they must be driven through some form of variable-ratio drive, which is governed to maintain substantially constant speed in spite of variations in engine speed. Furthermore, conventional alternators, if operating in parallel, must run at exactly the same speed to maintain synchronism. This entails rather severe requirements for the governors and variable-ratio drives,[2] although this problem is simplified to a certain extent by the use of free-wheeling between the drive and the generator.

Auxiliary gas engines provide a flexible source of power that does not interfere with the main power, and can be made of any required rating. The main disadvantages to such engines, at present, are their weight, space requirements, lower fuel economy (compared with that of the main engines), and relatively poorer reliability. In comparing fuel economy for a given flight, it is necessary to take into account the efficiency of a variable-ratio drive if used, and main-engine fuel economy as compared with that of the auxiliary engines. Also, the load duty cycle has an important bearing on this calculation. Where high altitude is involved, auxiliary engines either must carry their own superchargers or be supercharged from the main-engine supercharger. Auxiliary engine-driven generators are relatively easy to operate in parallel whether alternating or direct current.

The exhaust-gas turboalternator looks promising for certain applications in which a sufficient amount of energy can be extracted from the exhaust to furnish the electrical requirements without impairing the main-engine performance, as, for example, in medium-altitude cargo planes. Generators can be operated readily in parallel when employing this type of drive. In some high-altitude aircraft in which exhaust-gas energy is

Figure 2. Air-blast-cooled d-c aircraft generator, separately mounted, but driven from the main engine

5,400/11,200 rpm; 120 volts; $8^{3}/_{4}$ kw, continuous; $14^{1}/_{2}$ kw, five minutes; $17^{1}/_{2}$ kw, five seconds

Weight, 78 pounds; size, eight inches by eight inches by $14^{1}/_{2}$ inches long

used for supercharging cabins as well as engines, and also for deicing, there is not sufficient available energy remaining to provide driving power for a large electrical demand under all conditions of flight.

Waste heat from the exhaust can be utilized by a closed-cycle steam turbine. Such a drive naturally requires a boiler and condenser, as well as a turbine, and consequently may be difficult to justify on a weight basis. Since it operates on waste heat, however, it shows up well from the standpoint of fuel economy. For military use, a condenser having a large exposed surface would be objectionable because of its vulnerability. There are many other technical problems, such as condenser freezing at low ambients, that will have to be solved before this type of drive becomes useful in aircraft.

Wind-driven generators are used extensively on small airplanes for furnishing small amounts of power. However, they are very inefficient aerodynamically, and therefore are not competitive with other forms of drive when large amounts of power are required.

From the foregoing considerations it seems that accessory-shaft drive from main engines is the best power source available at present for the majority of large airplanes.

Types of Power Systems

The power system may take any one of a variety of forms, several of which are listed below.

1. Generate direct current and utilize direct current at substantially the same voltage, either 27 or 120 volts.

2. In some cases a dual-voltage d-c system may be used, with all the small equipment put on the 27-volt system and the larger equipment on the 120-volt system.

3. Generate variable-frequency alternating current, and rectify to utilize as direct current.

4. Generate constant-frequency constant-voltage alternating current; utilize part as alternating current, and rectify part for use as direct current.

5. Generate variable-frequency variable-voltage alternating current (voltage proportional to frequency), and utilize as generated.

6. Generate both alternating current and direct current, and utilize as generated.

7. Generate variable-frequency constant-potential alternating current; rectify and then invert to constant-frequency constant-potential alternating current.

There are many more possible combinations, but this group includes the varieties which have been used, or most seriously considered, to date.

The 27-volt d-c system has the advantages of simplicity and low hazard to operating personnel. It is satisfactory for most small and medium-sized planes. Where large amounts of electric power or large planes requiring long circuits are involved, this system becomes inadequate because of the large weight of the conductor required as a result of the limitations of conductor heating and line voltage drop. Commutation and brush wear at high altitudes are a maintenance problem, although great strides are being made toward minimizing it. This system is very widely used today.

The 120-volt d-c system overcomes the problem of excessive conductor size to a large degree, but introduces additional problems of commutation at higher voltages, particularly in small motors. It also introduces the problems of high-altitude higher-voltage switchgear and control. For energy-storage magnitude normally used for aircraft, batteries for 120-volt systems are quite heavy compared to batteries for 27-volt systems. The higher voltage is very suitable for large hoisting equipment and other similar apparatus which may be required in some cargo planes. Such systems have been used successfully and are to be included in certain aircraft now being constructed. Low voltage (27 volts) is usually required, in addition, for certain instruments, controls, and lamps (high-voltage lamp filaments do not resist vibration as well as low-voltage lamps of the same wattage).

The dual-voltage d-c system combines the advantages of the two d-c systems previously discussed with a slight added complexity.

Supplying direct current by rectifying the output of variable-frequency generators[10] makes it possible to use an alternator in place of the d-c generator. The absence of a commutator permits an alternator of much greater kilowatt capacity to be built for mounting directly on the main-engine take-off pad, within the prescribed limits of bending moment and diameter. Ratings as high as 32 kw have been built within these envelope limitations, which previously were occupied by six- to nine-kw d-c generators. This system also eliminates the need for high-current brushes on the generator, and therefore minimizes the maintenance problem. However, brushes are still used on the slip rings, so that the brush problem, although considerably reduced, is not eliminated entirely. The weight of a dry-plate rectifier, or transformer and rectifier, is added but this is compensated partially by the lower weight of the alternator as compared with a d-c generator of equivalent rating. Such systems are now under intensive development.

The constant-frequency a-c system offers a suitable source of power for most loads but requires variable-ratio transmission and governors if it is to be operated from the main engines. These naturally add appreciably to the weight. However, the advantages[1,9] of this system are sufficiently great so that now it is being given intensive development.

Use of auxiliary engines with this system eliminates the need of variable-ratio transmissions, but it is expected that such transmissions will weigh appreciably less than auxiliary engines. In general, the use of alternating current minimizes commutation and brush-wear problems, thereby decreasing maintenance. It does not eliminate these problems, however, since both exciters and slip rings may require brushes, although they do not present as difficult a problem as on completely d-c systems.

Variable frequency, with voltage proportional to frequency is satisfactory only for certain loads such as a-c torque motors. The difficulty with this system is that motors operating from it run at a speed depending upon the frequency, which varies widely. This would result in unsatisfactory performance of many devices. Other equipment, such as lights, radios, suit heaters, and so forth, normally operate at constant voltage and consequently special provision must be made for them. For these reasons, this

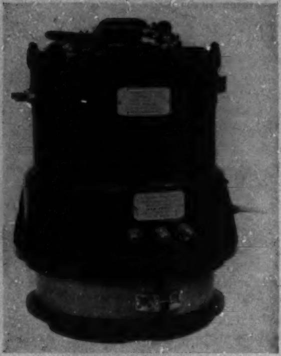

Figure 3. Dual-voltage d-c aircraft generator for use with auxiliary engine. The low-voltage generator also serves as a starting motor for the auxiliary engine

4,000 rpm; 120 volts, 6.75 kw and 28.5 volts, 2.0 kw; continuous; both machines are rated for 50 per cent overload for five minutes

Weight, 125 pounds, including coupling and cooling fan; diameter, 11 inches; length, 19½ inches

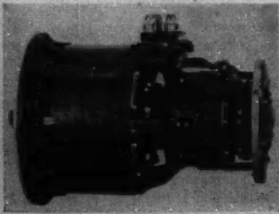

Figure 4. Air-blast-cooled a-c aircraft generator with direct-mounted exciter. Portion of housing and air duct removed to show exciter and collector rings. Driven from main engine at constant speed through variable-ratio transmission. Generator and transmission are mounted separately from the main engine

6,000 rpm; three-phase; 208Y/120 volts, 400 cycles, 40 kva, continuous; 60 kva, five minutes; 80 kva, five seconds; 0.75 power factor

Weight, 80 pounds, including exciter; diameter, 9¼ inches; length, 17¼ inches

type of system is used only for certain special applications.

Dual systems a-c and d-c, with separate power sources, combine certain advantages of other systems but increase complexity. They are used for special applications.

The rectified–inverted constant-frequency constant-voltage a-c system dispenses with the necessity for variable-ratio drives where main engines are used as the driving source. It also eliminates any need for synchronizing equipment and simplifies parallel operation. However, because of the weight of conversion equipment required and lower efficiency resulting from multiple transformation losses, this system does not seem very attractive at present.

The 27-volt d-c system is used most commonly in present-day medium and large aircraft, and it is likely to continue to be an important voltage level. Higher-voltage systems such as 120 volts direct current or 208 volts constant-frequency alternating current, supplemented by a small amount of 27-volt d-c distribution, appear promising for larger aircraft requiring greater amounts of auxiliary electric power. Much of the equipment for these higher-voltage aircraft systems is undergoing active development at the present time.

Figures 1, 2, 3, 4, and 5 show generators which have been developed for various systems.

Selection of Voltage

The selection of system operating voltage depends primarily upon the following factors, most of which can be evaluated in terms of weight.

1. Transmission distance.
2. Power to be transmitted.
3. Available utilization equipment.
4. Weight of reliable generation, distribution, and utilization apparatus.
5. Line-voltage-drop limitations.
6. Generation, distribution, and utilization of power at one voltage, insofar as possible, to avoid weight of transformers or converters.
7. Safety to personnel[3] and plane.
8. Creepage, insulation,[3,4] and arc-interruption problems.

In general, for increased power and increased transmission distances, higher voltage is desirable to minimize the weight in the power conductors. Mechanical limitations establish lower limits to wire size in aircraft wiring, and there is no gain in increasing the voltage beyond the point at which the minimum wire size can take care of a large part of the loads. As the voltage is increased, the required insulation increases, and consequently the point of diminishing returns is reached rapidly.[6] Also, the hazard to the operator increases with increasing voltage.[3] It is therefore, desirable, to select a practical voltage level no higher than that which will give minimum over-all weight.

The influence of system voltage on the weight of the power-distribution-system copper based on the same power, transmission distance, and current density is illustrated by the accompanying table:

Systems	Per Cent
27 volts, direct current, with ground return	100
120 volts, direct current, with ground return	22.5
120 volts, direct current, two-wire	45
208Y/120 volts, three-wire, three-phase, alternating current, 0.75 power factor	30

Alternating Current Versus Direct Current[1,9]

The functions of the utilization equipment and the operating conditions will largely determine whether alternating current or direct current should be used. For example, radios normally require alternating current, whereas direct current is best adapted to many control devices.

For large high-altitude aircraft, alternating current is so promising that a great deal of development work on equipment for such systems is now in progress. The simplicity, low maintenance, safety, and light weight of a-c motors and generators tend to favor its use. Commutation and brush-wear problems are minimized. Because of commutator-design limitations, development of very high-speed motors appears more promising with alternating current. Also, certain equipment requires alternating current, which can be obtained directly from an a-c system, thereby eliminating the need for converters. Alternating current has the advantage that various desired utilization voltages can be obtained by transformers, which are lighter and simpler than d-c voltage converters.

Direct current has the advantage that generators may be driven directly from engine take-off shafts without variable-ratio constant-speed transmissions. For sizes up to 12 kw they can be mounted directly on the engine pad. Also, paralleling of d-c generators is relatively easy. D-c motors are capable of greater peak torques and starting torques than are a-c motors of the same weight. They are, also, more adaptable to speed control. Single-wire distribution, with ground return and single-pole switching, is possible with direct current on metal airplanes. Wiring weight is less, at the same voltage, because it is not necessary to transmit the wattless kilovolt-amperes associated with alternating current at power factors other than unity. Energy storage, by use of batteries, is possible with direct current.

Sufficient data are not yet available for a complete weight comparison between a-c and d-c systems, but they are being accumulated as equipment designs are developed. Any such comparison must consider the weight of all equipment, such as generators, motors, wiring, control, variable-ratio transmissions, circuit breakers, contactors, fuses, et cetera, and also the weight of fuel required to supply the losses in the system for the duration of the flight.

Selection of Frequency if Alternating Current Is Used

The selection of frequency[1] depends to a large extent upon the type of utilization equipment used in the airplane. In general, selection should be made for minimum weight of the entire electric system which will provide satisfactory performance. It is desirable to operate motors at high speeds up to the limits of good bearing performance, since this tends to minimize equipment weight even when reduction gears are required. The frequency should be high enough to permit motor speeds in the order of 8,000 to 12,000 rpm and, yet, not so high as to require, at such speeds, too many motor poles for efficient motor design. Much of the d-c motor-driven apparatus used on present-day aircraft is designed for motor speeds of 8,000 rpm. This has been found satisfactory for continuous operation of grease-packed ball bearings. For intermittent operation, grease-packed ball bearings in small sizes operate satisfactorily up to 12,000 rpm. It is desirable to utilize these same speeds insofar as possible, to avoid unnecessary redesign of gear-reduction mechanisms, and to make use of maximum speed permitted by practical bearing design. It is desirable, therefore, to select a frequency which will provide these speeds.

The frequency should be selected so that speeds up to 24,000 rpm are not ruled out, since such speeds may be desired for the lightweight turboalternators which hold promise for the future. Such machines probably would be designed with oil-pressure lubrication on the bearings. Again, certain gyroscopic instruments may require speeds of this order.

For motors that are not geared, the frequency should be such that at least the majority of these motors can be designed with a number of poles (preferably four, six, or eight) which will result in the required speed with an efficient, lightweight design. The frequency should be high enough so that a sufficient number of speed steps are available in the desired motor-speed range to permit flexibility in application without requiring an excessive number of poles.

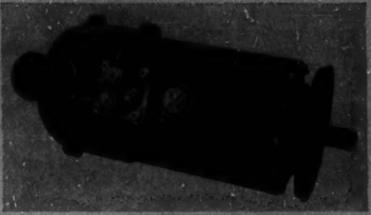

Figure 5. Oblique view of main-engine-mounted air-blast-cooled d-c and a-c aircraft generator

4,000/8,000 rpm; 200 amperes; 30 volts direct current. 800/1,600 cycles; single phase; ten amperes; 120 volts alternating current

Weight, 47 pounds; diameter, six inches; length, $13^1/_2$ inches

Iron-core transformers become lighter with increasing frequency within practical limits, so that this becomes a consideration in applications where many transformers are used.[11]

There is a considerable amount of 115-volt 400-cycle single-phase a-c electronic equipment now in use in aircraft. From the standpoint of interchangeability and elimination of conversion equipment it is desirable to be able to operate this electronic equipment directly from the power system.

If too high a frequency is used, skin effect in conductors and eddy current and penetration effects in iron cores increase the weights of wiring and equipment. Except for frequencies well above 400 cycles, however, these effects are not significant for most wire sizes and equipment designs used in aircraft. For example, for all wire sizes equal to or smaller than AN–8,* the skin and proximity effects at 400 cycles increase the losses by less than one per cent. This includes the majority of wires on the 208Y/120-volt system now contemplated. For larger wire sizes, skin and proximity effects increase considerably. Thus, for AN–0 it is 11 per cent, and for AN–0000 it is 30 per cent at 400 cycles. These figures are based on minimum practical spacings with insulated aircraft cables.

Polyphase Versus Single Phase in A-C Systems

Single-phase systems offer simplicity in wiring and switching equipment. Single-phase motors require capacitors or other means of phase splitting in order to obtain starting torque. Polyphase motors and generators are lighter than single-phase motors because they utilize available winding space more effectively.** Systems with three or more phases will transmit power with less copper than systems with a fewer number of phases. Generally there is no advantage in using more than three phases. Of course, single-phase loads can be operated from three-phase systems satisfactorily, provided they are reasonably well distributed among the phases to avoid excessive unbalance. Single-phase motors will be used only for very small incidental loads where three-phase power is available.

Types of Alternators for A-C Systems

The alternator may take any one of several forms, some of which are as follows:

1. Conventional design, with either rotating field or rotating armature.
2. Inductor alternator, having rotating salient poles excited by a stationary winding.
3. Induction alternator, having a squirrel-cage rotor.

The conventional design requires two sets of slip rings and brushes for the rotating field, and three or four sets for the rotating-armature three-phase machine, depending on whether or not the neutral is brought out. The light weight of these generators, their stability when used with direct-connected exciters, and ease of control make the conventional type of machine preferable for most cases.

The inductor alternator requires no slip rings or brushes of any kind, but its weight is from 40 to 50 per cent greater than that of the conventional type, so that it is not being developed actively for main-power generation.

The induction alternator also requires no slip rings, and can be built with a simple, sturdy rotor similar to that in a squirrel-cage induction motor. For this type of generator, parallel operation is greatly simplified, since the machine takes its excitation from the system; consequently, synchronization and precise speed control are not required, although a variable-ratio drive is required. Where such alternators are used, however, excitation must be furnished by other synchronous machines on the system, either alternators or synchronous condensers. The weight and vulnerability of these additional necessary machines is a disadvantage. Also, the ability of the system to recover from electrical disturbances tends to be less than for systems of comparable weight using conventional generators with direct-connected exciters. The starting problem also must be given special consideration with this system, as special devices are required for this purpose.

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* Army-Navy Standard Aircraft Wire Size. Approximates corresponding American Wire Gauge size.

** Except in very small sizes (1/[illegible] horsepower and less) when it may not be practical to use three-phase windings because of space limitations.

Types of Excitation Systems for A-C Systems

If conventional alternators are used, excitation can be furnished by:

1. A direct-connected self-excited exciter.
2. Transformation and rectification from the a-c system.
3. D-c generators or batteries on the plane, if available.

The direct-connected exciter is preferred because this arrangement makes each unit independent of other units on the system. It also provides a relatively "stiff" system which will not collapse completely under excessive load or even under solid-fault conditions, since a fault or load does not reduce the available excitation. The disadvantage of this system is the added weight and space required by the exciter, and the fact that brushes and commutator are present on the exciter. However, since the energy requirements for the exciter are relatively small, this does not appear to be a serious problem. A direct-connected exciter also results in lighter voltage-regulating equipment, since only exciter field current need be regulated, and standard voltage regulators that already have been developed for aircraft can be used with only slight modifications.

Excitation from rectifier supply must depend either upon residual in the alternators or upon the presence of an independent d-c system for initial build-up. A collapse of the a-c system through a sustained solid three-phase fault will mean a loss of excitation and, consequently, the inability to burn the fault clear. It also makes a system that is less stiff and has a maximum current point beyond which both the voltage and excitation will collapse unless the overload or fault is removed very quickly. This system introduces starting problems, particularly at low ambient temperatures at which the rectifier resistance increases. If high residual is incorporated to assist in starting, the regulator range required is increased considerably.

Excitation from an independent d-c system has the disadvantage that damage in the d-c system (a fault, for example) is communicated immediately to the a-c system as a loss of excitation, thereby tending to depress the a-c voltage also. It also requires heavier nonstandard voltage-regulating equipment compared with that for direct-connected exciters.

Manner of Grounding

Most 27-volt d-c systems are solidly grounded to eliminate one wire in metal planes. This not only reduces the weight of wiring but also the exposure to hazards such as gunfire. One disadvantage of grounding is that any energized conductor accidentally coming in contact with the metallic skin of the plane constitutes a fault on the system.

Higher-voltage d-c systems probably also will be grounded to eliminate one wire. While a perfectly isolated system would involve less hazard to operating personnel than a grounded system, there is always in practice, a certain amount of capacitance and leakage between conductors and ground and also the possibility of accidental grounds, so that it is doubtful whether grounding significantly affects electric-shock hazard.

When both voltages of a dual-voltage d-c system are grounded, a third voltage is made available, which is either the sum or difference of the other two voltages, depending on whether like or unlike polarities on the two systems are grounded.

Some of the usual reasons for a-c system grounding, such as protection against transient overvoltages (on systems above 600 volts) and possible improvement of lightning-arrester protection for overhead transmission systems, are not important in aircraft.

Grounding of aircraft systems has the advantage that accidental faults either are cleared immediately or otherwise detected so that faults are not likely to go unnoticed. Ground detectors can be used, of course, with an ungrounded system, but they will not indicate the location of the fault. Accidental grounds, in an ungrounded system, which are not detected constitute a hazard, since the next ground on another conductor puts a fault on the system. Grounding the neutral of a three-phase a-c system on a metal airplane provides the equivalent of a four-wire system, so that single-phase loads can be operated conveniently at line-to-neutral voltage. Furthermore, if polyphase motors are operating on the system and their neutrals are grounded, they will continue to operate reasonably well if one circuit is accidentally open-circuited (by enemy gunfire, for example). This is considered to be an advantage on certain types of equipment for military planes.

Grounding of the neutrals of a-c generators increases the line-to-ground fault current of the system. If the utilization equipment is ungrounded, this ground fault current can be limited to any desired value by a suitable resistor or reactor in the generator neutral. However, in cases where the motors in the utilization equipment have solidly grounded neutrals, such a current-limiting device does little to limit the line-to-ground current. See Figure 6.

Whereas grounding the neutral solidly increases the fault currents which must be handled by the switchgear, it also prevents displacement of the neutral so that the switch voltage is reduced; hence the weight of switching equipment is not necessarily increased. This is shown by Figure 7. Higher ground-fault currents speed up the "burning clear" of ground faults and make it more difficult to obtain a "welded" fault.[4] They shorten the fault clearing time for fuses which may be used in the circuits.

Grounding of one phase of a three-phase a-c system has been given consideration, but is not being planned for any specific projects to date. The weight of one wire is saved, of course, by this means, and switches may be only two pole. However, line-to-neutral voltage is not available then, switchgear must handle 73 per cent higher voltage per pole, electric-shock hazard is somewhat increased by the higher line-to-ground voltage, and an increase of radio and interphone interference resulting from the presence of unbalanced fields may be introduced.

Power-Factor Correction

Capacitors frequently are used for power-factor correction in industrial a-c systems. The benefits are reduction in line losses, reduced wire size to transmit a given amount of power, and increased power available from a given-size generator. In higer-hfrequency a-c systems such as the 400-cycle system, the weight of capacitors required per kilovolt-ampere of correction is substantially less than that in 60-cycle systems of the same voltage. Capacitor weights of the order of $1^{1}/_{4}$ pounds per kilovolt-ampere, in sizes of one-half kilovolt-ampere and larger, are now available for 208-volt 400-cycle systems, and future developments may reduce this considerably.

In spite of these relatively low weights for corrective capacitors for 400-cycle a-c systems, it is doubtful if they will have widespread application in aircraft, because the lines are relatively short and the loading intermittent. Preliminary calculations on certain systems indicate that the savings in wire weight, losses, and generation are offset by the weight of the capacitors. Such a calculation can be made for any given system to determine whether or not power-factor correction is justified. In general, the lower the power factor, the more effective are the capacitors, so that for some very low-power-factor apparatus they can be justified. It usually does not pay to correct to unity power factor, however.

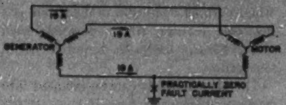
In addition to the weight and economy, there are certain other factors that should be considered in any particular case in determining whether or not power-factor correction is desirable. The capacitors occupy a certain amount of space and, therefore, tend to increase the vulnerability of the system to gunfire, for example, and to accidental fault. In addition, where parallel operation is involved, the system stability is reduced if full advantage is taken in reduction of possible generator size.

Parallel Versus Nonparallel Operation

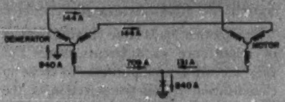
When several generators are used, parallel operation provides certain advantages and has certain drawbacks. The most important of these are given in the following tabulation, and apply to both a-c and d-c systems.

ADVANTAGES

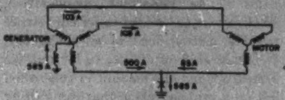
1. Greater motor-starting capacity.
2. Reduction of time and magnitude of voltage disturbances from faults, motor starting, or loss of generation.
3. More effective use of spare capacity, so that smaller or fewer generators can be used without excessive load-transfer switching.
4. Greater ability to burn faults clear.[4]
5. Continued operation of load equipment following engine or generator failure does not require any automatic transfer equipment nor any immediate attention from the operator.



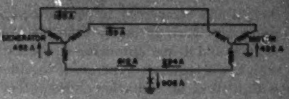
A. Generator neutral ungrounded, motor neutral ungrounded



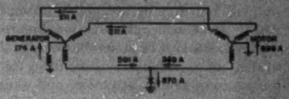
B. Generator neutral directly grounded, motor neutral ungrounded



C. Generator neutral grounded through a reactor ($X_n = 5\%$), motor neutral ungrounded



D. Generator neutral directly grounded, motor neutral directly grounded



E. Generator neutral grounded through a reactor ($X_n = 5\%$), motor neutral directly grounded

Assumptions:

Generator rated current = 100 amperes
Motor rated current = 100 amperes
Generator internal voltage = 100 per cent

Reactances	Generator (Per Cent)	Motor (Running Light) (Per Cent)
Positive sequence, X_1	20	500
Negative sequence, X_2	20	20
Zero sequence, X_0	5	5

Figure 6. Effect of neutral grounding and neutral impedance on line-to-ground fault current

DISADVANTAGES

1. Increases weight and complexity of control, especially for alternating current. Equipment for synchronizing is required for paralleled a-c systems. Constant-speed transmissions for a-c systems require accurate governing if generators are to be paralleled (with auxiliary-engine drive this problem is not so severe). For d-c generators, voltage regulators must control within very close limits to permit satisfactory parallel operation. Furthermore, equalizing circuits must be designed and maintained carefully, especially if a large number of generators is used.
2. Routine monitoring of load balance and (for alternating current) reactive kilovolt-ampere balance may be required.
3. Faults and other disturbances affect entire electric system.
4. Heavier switchgear is required because of the greater fault currents available.
5. Co-ordination problems relating to circuit protective equipment are more exacting because of the possibility of variable generation, and the higher currents involved.

No general rule can be made since the selection here depends, to a large extent on the type of utilization equipment and the nature of the operations involved. Both parallel and nonparallel operation have been used successfully in airplanes. A-c systems which are subject to loss of synchronism through fault or other disturbances should be designed so that they can operate either parallel or nonparallel.

At present, considerable development work is being pursued in order to establish the practicability and performance requirements of parallel operation of aircraft alternators from engine take-off drives using variable-ratio transmissions.

Synchronizing Methods for Paralleling A-C Generators[3]

If the system selected is alternating current, and parallel operation is desired, the question of how best to synchronize the generators is introduced. There are several methods available. These are:

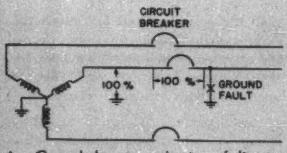
1. Automatic synchronizing, with full voltage on both running and incoming machines.
2. Automatic synchronizing, with reduced or zero voltage on the incoming machine.
3. Manual synchronizing, with either full, reduced, or zero voltage on the incoming machine.
4. Random manual synchronizing at full voltage.

It is probable that purely random synchronizing, without any attempt to match the phase angles of the generators before switching them together, will cause undue momentary disturbance to the system voltage. At present, considerable development is in progress on equipment for fully automatic synchronizing, which will parallel a-c generators with practically no resulting disturbance. However, this involves appreciable added weight and complexity in control equipment, so work is being done also on simpler and lighter automatic equipment, which will connect generators together with little or no excitation on the incoming machine, building its excitation up immediately after switching. This will cause some disturbance to the system voltage, but may be acceptable in some cases. In general, automatic synchronizing is expected to be preferable to manual means, since manual synchronizing requires considerable attention and skill on the part of the operator.

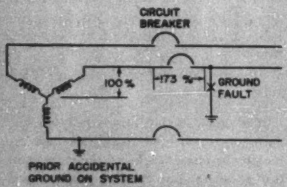
Size and Number of Generators

The number of generators is selected by considerations of vulnerability and amount of stand-by power desired for emergency use. Generators should be distributed among engines as far as is practicable, so that loss of one engine will not cause too great a loss of generation. From the standpoint of weight economy, it is important that the airplane not be burdened with excessive generating capacity. Stand-by capacity of 100 per cent is considered to be the upper limit, even for military combat planes.

It is desirable to select generators which individually are large enough to start the largest single load (or group of loads which must be started simultaneously). This may seem unnecessary when parallel operation is used, but is a desirable feature, since operation from one generator may be required in an emergency.



A. Grounded system, clearing of line-to-ground fault



B. Ungrounded system, clearing of line-to-ground fault. (As shown, this fault will not cause breaker tripping unless an accidental ground already exists on the system)

Figure 7. Effect of grounding on recovery voltages. (Voltages are given in per cent of normal line-to-ground voltage)

The electric loads can be grouped into two main categories, namely:

1. Those essential to flight.
2. All other loads.

These other loads have varying degrees of importance in different planes but they are always subordinate to those in classification 1. In the selection of the amount of stand-by power, this division and the degree of importance of other loads is an important consideration, since service to loads of lesser importance may be discontinued in an emergency. It is desirable, of course, to lay out the system so that the amount of generation left on a crippled plane that can still fly will be sufficient to bring the plane back safely.

In totaling the electric load on an airplane, duty cycle and time of operation are important considerations, since many of the loads are highly intermittent in nature. The peak load requirement must be met with suitable stand-by, of course. Short-time overload ratings are given on aircraft generators to permit proper selection for such conditions.

Use of Auxiliary-Engine-Driven Generators to Supplement Main-Engine-Driven Generators

An auxiliary set, either hand- or battery-started, is of value for ground operations such as electrical testing and starting the main engines.

Such a set usually is not supercharged. However, at low altitudes it could be used to furnish some essential power in an emergency, provided that the unit is installed in such a manner that it can be started after hours of flight at extremely low temperatures.

In cases where the weight of such a unit might be considered excessive, it can be treated as disposable load, provided that the airplane always is to be based where auxiliary ground power is available or a battery is used that will handle all essential ground operations, such as engine starting in cases where electric starters are used.

Satisfactory, small, independent, lightweight superchargers, suitable for use with small auxiliary engines in aircraft which must operate at high altitude, and which are subject to sudden changes of load, have not yet appeared. However, it is possible to utilize the first stage (turbine- or gear-driven) of the main-engine supercharger for this purpose. Another possibility is to utilize air from the cabin supercharger (or cabin waste air), if available in sufficient quantity to meet the requirements, and if the supply is dependable under all conditions of operation.

Use of an Auxiliary D-C System With Main A-C Power System

Certain control functions, instruments, and other small electric loads are handled most economically from a low-voltage d-c system and, consequently, it seems desirable to maintain such a system even in cases where the main power is alternating current. Such a system may be derived from the main a-c system by suitable transformation and rectification. It may or may not have a battery. If no battery is used, it is important to arrange the circuits in such a manner that the a-c system can be started, preferably without the use of auxiliary ground power.

Distribution-System Design[7]

The fundamental principles of good distribution-system design are the same for all types of systems. Protective equipment for generators, busses, lines, and utilization equipment for d-c and a-c systems, of all voltages used so far in aircraft, are available or are being developed. It is beyond the scope of this paper to discuss the various means and devices for insuring reliable distribution-system performance with minimum weight. This weight should be evaluated in each specific case when selection among types of power systems is to be made.

Conclusions

There are many conflicting requirements for different applications, but the need for higher-voltage higher-power systems has required industry to develop apparatus rapidly to meet this demand. Consideration of the previously enumerated conditions and requirements has resulted in the selection of the 208Y/120-volt 400-cycle three-phase grounded-neutral system as one standard for a-c systems in aircraft. This choice represents a compromise of the many factors involved. All parts of these systems are now undergoing intensive development. These developments entail an enormous amount of engineering and development work, as practically every piece of apparatus in the generation, distribution, and utilization systems has to be designed especially for 400 cycles and aircraft use, which requires lightweight, vibration-resistant designs operable at high altitude.

The selection of 208Y/120 volts, 400 cycles, and three phases as a voltage and frequency level for a-c systems in aircraft appears to be a particularly happy choice, in that this voltage is high enough to transmit the required amounts of electric power economically over the distances required by the largest planes and high enough for the largest amounts of auxiliary electric power in any planes now being designed or likely to appear in the near future. This voltage is low enough so that no unusual difficulties are experienced in switching, even at high altitudes. It is below the range where corona at high altitude becomes troublesome.[6] Again, it is low enough so that it does not constitute a very serious hazard to operating personnel from accidental contact. Four hundred cycles is somewhat less hazardous in this respect than 60 cycles.[3]

A considerable amount of equipment in military planes is now served by 115-volt 400-cycle single-phase inverters or dynamotors operating from the 27-volt d-c system. This apparatus can be served directly from the 208Y/120-volt 400-cycle system, which probably will be adjusted to hold about 205 volts at the generator bus so that the utilization-equipment voltage will be about 200Y+115. In this way, no transformers will be required to serve this apparatus, and the need for dynamotors or inverters will be eliminated, with a consequent saving in weight.

The proposed system will retain a relatively small 27-volt d-c system derived from the main a-c system for control, instruments, and other small loads as required. Generators will be of the conventional type with direct-connected exciters.

Some work has been and is being done on 120-volt d-c equipment. It is probable that dual-voltage d-c systems (27 and 120 volts) will make their appearance in medium-altitude cargo or transport airplanes where commutation and brush problems are not so severe and the equipment is of the nature of hoisting equipment or similar apparatus for which this voltage is well adapted.

Thus the aircraft industry will have the following voltage levels for which apparatus already is available or is being designed:

1. For very small planes, such as private planes, 6- or 12-volt d-c systems.
2. For small and medium-sized planes, 27-volt d-c systems.
3. For large medium-altitude cargo or transport planes, dual-voltage (27- and 120-volt) d-c systems.
4. For large high-altitude long-range planes, 208Y/120-volt three-phase 400-cycle a-c systems.

These voltage levels will probably meet the needs of the aircraft industry for some time to come.

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Starting Windings for Single-Phase Induction Motors

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Synopsis: Methods are given to enable the design engineer to determine the best starting winding and the best capacitor, reactor, or resistor required to meet any specified requirements. The methods given have been in actual use by design engineers for over 12 years, and are as useful today as they were when developed. Simple circle diagrams depict graphically the principles involved in the method. Some practical observations, based on experience, are given. A calculation form, with sample calculations, is also included.

CALCULATION METHODS given in this paper supply the designer with a tool for computing the starting performance of any given winding; they also enable him to determine the particular auxiliary winding best suited to meet any given set of requirements. In this paper use is made of the equivalent-circuit concept so popular in technical writing. Also, use is made of the concepts of *open-circuit reactance* and *short-circuit reactance*, introduced by Branson in 1912,[4] followed by Kron,[5] Boothby,[1] Puchstein and Lloyd,[6] Himebrook,[7] Trickey,[8] and by the author in previous papers.[2,9]

Locked-Rotor Impedance of a Single Winding

A necessary preliminary to the determination of starting performance is the determination of the locked-rotor impedance. Very roughly, the locked-rotor impedance is

$$\dot{Z}=(r_1+r_2)+j(x_1+x_2) \qquad (101)$$

This simple relationship usually is not accurate enough for fractional-horsepower motor design. A more accurate expression, given as equation 2 in Appendix I, is

$$\dot{Z}=(r_1+r_2C_r)+jXC_R \qquad (102)$$

The coefficients C_r and C_R are defined in equations 9 and 10 and plotted in the form of curves in Figure 2. It will be observed that the apparent rotor resistance is always less than the actual, while the apparent short-circuit reactance is always greater than the ideal. In the derivations that follow, the apparent impedance, as defined above, is used consistently.

Starting-Torque Equation for Single-Phase Induction Motors

C. R. Boothby[1] gave the following equation for the starting torque of a single-phase induction motor with an auxiliary winding. Expressed in the notation of this paper, and with one modification, his equation is

$$T_s=\frac{1.88p}{f}Kr_{2m}'I_mI_a\sin(\theta_m-\theta_a) \qquad \text{(ounce-feet)} \qquad (103)$$

The modification from this original equation is the use of the apparent rotor resistance, r_{2m}', instead of the actual rotor resistance, r_{2m}. (See Table I.) Morrill[3] has given a more rigorous derivation of the starting-torque equation; equation 14 of his paper is exactly reducible to equation 103. This starting-torque equation is applicable to all types of single-phase induction motors having two windings in space quadrature; it is the basis for the remainder of this paper.

Starting Windings for Split-Phase Induction Motors

The design of the main or running winding usually is fixed by considerations of efficiency and breakdown torque. The problem generally is to find the best possible companion starting winding; in this connection, considerable help will be given by Figure 3, which shows in a graphical manner how various changes in auxiliary winding affect the starting torque and line current.

The magnitude and phase position of the locked-rotor current of the main winding is computed from equation 102 and laid out in Figure 3 as OA. A certain number of turns and distribution of the starting winding is assumed, fixing the reactance, X_a'. The problem now is to determine the optimum resistance for this assumed starting winding. The semicircle AGB is constructed with $AB=E/X_a'$. Now, take any point on this semicircle, C, for example; the line AC represents the magnitude and phase position of the auxiliary-winding current, I_a, for some as yet unknown value of R_a and the assumed value of X_a'. In other words, the semicircle is the locus of the end of the vector I_a as R_a is varied, all other quantities remaining constant. The line current is OC or the vector sum of the main- and auxiliary-winding currents. The starting torque is proportional to the length of CK, since

$$CK=I_a\sin(\theta_m-\theta_a)$$

The maximum starting torque that can be obtained by varying R_a and leaving all other terms fixed occurs when CK is a maximum in length. This condition is fulfilled by point D on the diagram, where D is midway between A and G on the arc. The maximum starting torque per ampere of line current is obtained when the ratio CK/OC is a maximum, which occurs when the line OC is tangent to the semicircle. In the figure, C is actually the point for maximum starting torque per ampere.

This diagram shows a considerable increase in line current between points C and D for an almost negligible gain in starting torque. The diagram is most helpful in visualizing the factors involved and their interrelationships. It is not recommended as a routine design procedure; algebraic equations are better

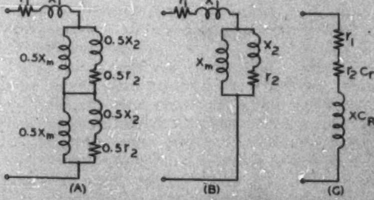

Figure 1. Equivalent circuit of a single-phase induction motor at standstill (revolving-field theory)

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suited to this purpose. Such equations may be developed from the diagram. First, two distinct starting windings will be recognized: a *maximum-torque winding* as indicated by point D, and a *maximum-torque-per-ampere* winding, as indicated by point C.

Maximum-Torque Windings for Split-Phase Motors

For the maximum-torque winding

$$\widehat{AD}=\frac{\widehat{AG}}{2}$$

therefore, by plane geometry

$$\angle DAH=\theta_a=\frac{\theta_m}{2} \qquad (104)$$

By trigonometry

$$\cot\theta_a=\cot\frac{\theta_m}{2}=\frac{1+\cos\theta_m}{\sin\theta_m}=\frac{1}{\sin\theta_m}+\cot\theta_m$$

or

$$\frac{R_a}{X_a'}=\frac{R_m+Z_m}{X_m'} \qquad (105)$$

or

$$R_a=\frac{X_a'}{X_m'}(R_m+Z_m) \qquad (106)$$

The total resistance in the primary of the starting winding is

$$r_{1a}=R_a-r_{2a}' \qquad (107)$$

Equation 106 gives the total value of resistance that must be used in the primary winding to obtain a maximum-torque winding as shown by point D in Figure 3.

At the AIEE North Eastern District technical meeting in Rochester, N. Y., in 1931, P. H. Trickey presented a student paper on starting windings in which, by differentiating a form of equation 103 and finding the value of r_{1a} to give a maximum T_s, he found that

$$r_{1a}=K^2(r_{1m}+Z_m) \qquad (108)$$

which is substantially equivalent to equation 106. The comparison is interesting. Trickey's student paper goes on to investigate the maximum-torque winding quite extensively. Unfortunately, however, this winding is seldom the best one to be used, as it results usually in the use of more copper in the starting winding than necessary and sometimes results in low pull-up torque.

Maximum-Torque-Per-Ampere Windings for Split-Phase Motors

Point C in Figure 3 defines a maximum-torque-per-ampere winding. It is shown in Appendix II that the value of R_a, corresponding to point C, is

$$R_a=\frac{R_mX_a'+Z_m\sqrt{X_a'(X_m'+X_a')}}{X_m'} \qquad \begin{matrix}(109)\\(20)\end{matrix}$$

Equation 109 gives the total value of resistance that must be in the auxiliary phase to obtain a maximum-torque-per-ampere winding; the total resistance that must be used in the primary winding is found by subtracting r_{2a}', as done in equation 107.

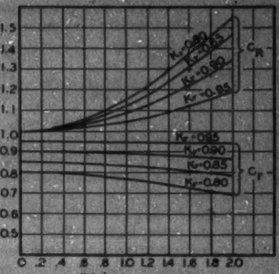

Figure 2. Variation of C_R and C_r with change in motor-design constants

Calculation of a maximum-torque-per-ampere winding using the form of Figure 6 can be simplified by rewriting equation 109 in the form

$$X_m'R_a-R_mX_a'=Z_m\sqrt{X_a'X_{ma}'} \qquad (109a)$$

More useful than the above are equations giving the required values of R_a and X_a' in terms of the desired locked-rotor current, since the latter quantity usually is specified. For a maximum-torque-per-ampere winding designed so that the total locked-rotor current in the line is I_L, it is

Table I. Motor Constants for Locked-Rotor Conditions

	Windings Only		Windings With External Impedance in Series		
	Main Winding	Auxiliary Winding	Auxiliary Winding With Resistor	Auxiliary Winding With Capacitor	Main Winding With Reactor
1. Applied volts	E	E	E	E	E
2. External impedance	O	O	Z_r	Z_c	Z_R
3. Real component of external impedance	O	O	R_r	R_c	R_R
4. j component of external impedance	O	O	X_r	X_c	X_R
5. Rotor resistance, referred to primary winding concerned	r_{2m}	r_{2a}	r_{2a}	r_{2a}	r_{2m}
6. Apparent rotor resistance	$r_{2m}'=C_rr_{2m}$	$r_{2a}'=C_rr_{2a}$	$r_{2a}'=C_rr_{2a}$	$r_{2a}'=C_rr_{2a}$	$r_{2m}'=C_rr_{2m}$
7. Resistance of primary winding	r_{1m}	r_{1a}	r_{1a}	r_{1a}	r_{1m}
8. Real component of total impedance	$R_m=r_{1m}+r_{2m}'$	$R_a=R_{1a}+r_{2a}'$	$R_{ar}=R_a+R_r$	$R_{ac}=R_a+R_c$	$R_{mR}=R_m+R_R$
9. Ideal reactance	X_m	X_a			
10. Apparent reactance, or j component of total impedance	$X_m'=C_RX_m$	$X_a'=C_RX_a$	$X_{ar}'=X_a'+X_r$	$X_{ac}'=X_a'+X_c$	$X_{mR}=X_m'+X_R$
11. Total impedance	$Z_m=\sqrt{R_m^2+X_m'^2}$	$Z_a=\sqrt{R_a^2+X_a'^2}$	$Z_{ar}=\sqrt{R_{ar}^2+X_{ar}'^2}$	Z_{ac}	Z_{mR}
12. Current in one phase	$I_m=E/Z_m$	$I_a=E/Z_a$	$I_{ar}=E/Z_{ar}$	$I_{ac}=E/Z_{ac}$	$I_{mR}=E/Z_{mR}$
13. Angle of lag of current behind the voltage	θ_m	θ_a	θ_{ar}	θ_{ac}	θ_{mR}
14. Defining equation for angle of lag	$\tan\theta_m=\frac{X_a'}{R_m}$	$\tan\theta_a=\frac{X_a'}{R_a}$	$\tan\theta_{ar}=\frac{X_{ar}'}{R_{ar}}$	$\tan\theta_{ac}=\frac{X_{ac}'}{R_{ac}}$	$\tan\theta_{mR}=\frac{X_{mR}}{R_{mR}}$

shown in Appendix II that the starting winding has the following constants:

$$R_a = \frac{R_m + Z_m(I_L/I_m)}{(I_L/I_m)^2 - 1} \qquad \begin{matrix}(110)\\(25)\end{matrix}$$

and

$$X_a' = \frac{X_m'}{(I_L/I_m)^2 - 1} \qquad \begin{matrix}(111)\\(18)\end{matrix}$$

where I_m = locked-rotor current in main winding only.

Of the equations given so far, 110 and 111 are the most useful. They completely specify a starting winding that gives the maximum starting torque per ampere of line current for a given main winding and specified value of total locked-rotor current. In using these equations, however, the current density in the starting windings must be checked, for sometimes these formulas lead to an overworked starting winding.

In general, the maximum-torque-per-ampere winding results in less weight of starting-winding copper and works the starting winding harder than the maximum-torque winding. Practical windings often are a compromise between the two, and generally favor the former, as far as possible.

Alternate Formula for Starting Windings for Split-Phase Motors

The circle diagram of Figure 3 is based upon the assumption that R_a is the variable and all other factors remain constant. H. E. Ellis has derived an interesting equation from equation 103, assuming X_a' to be the variable and all other factors remain constant. He assumed

$$K \propto \sqrt{(X_a'/X_m)} \qquad (112)$$

K thus becomes a variable in his analysis.

Equation 103 can then be rewritten

$$T_s = \text{constant}\left(\sqrt{\frac{X_a'}{X_m'}}\right)\cdot\left(\frac{X_m'R_a - R_mX_a'}{Z_m(R_a^2 + X_a'^2)}\right) \qquad (113)$$

$$\frac{dT_s}{dX_a'} = X_a'^3 - 3X_a'^2\left(\frac{X_m'R_a}{R_m}\right) - 3X_a'R_a^2 + \frac{X_m'R_a^3}{R_m} = 0 \qquad (114)$$

Solving for X_a', one finds that the only practical root is

$$X_a' = \frac{R_a}{R_m}\left(X_m' - 2Z_m \sin\frac{\theta_m}{3}\right) \qquad (115)$$

An excellent procedure is to calculate R_a from equation 110, and X_a' from equation 115, in place of equation 111. This procedure is perhaps the best but, of course, the current density in the starting winding has to be watched.

Resistance-Start Motors

A resistance-start motor differs from a split-phase motor only in that an external resistance is used in series with the starting winding. In such a case, R_a as used in the preceding expressions, must be assumed to include the resistance of the external resistor as well as the resistance of the motor windings.

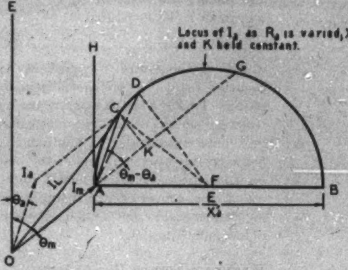

Figure 3. Starting-torque diagram for a split-phase motor

Capacitor-Start Motors

The locked-rotor conditions of a capacitor-start motor are illustrated in Figure 4.* Both main and capacitor windings are assumed. The magnitude and phase position of the main-winding current I_m is constructed as OA. A value of capacitance has been assumed, and the capacitor-phase current I_a is laid out similarly as AC. Now, if we make the assumption that R_{ac}, the total resistance in the capacitor phase, remains constant, the locus of point C, as the external microfarads vary, will be the semicircle shown having a diameter of E/R_{ac}. This assumption is not strictly correct, for R_{ac} will vary slightly as the microfarads are changed (because R_c does), but it leads to convenient and useful formulas, and the slight variation in R_{ac} with microfarads can be taken into account, as will be shown later.

The point for maximum starting torque is D, found by drawing a tangent parallel to OA. It is shown in Appendix III that the maximum starting is obtained when

$$-X_{ac}' = \frac{R_{ac}}{R_m}(Z_m - X_m') = \frac{R_{ac}R_m}{Z_m + X_m'} \qquad \begin{matrix}(116)\\(28)\end{matrix}$$

(X_{ac}' from equation 116 is always a negative number.)

The reactance of the external capacitor is simply

$$X_c = X_{ac}' - X_a' \qquad (117)$$

where X_{ac}' is found from equation 116. (Since X_{ac}' and X_c are both negative, $-X_c$ is the arithmetical sum of X_{ac}' and X_a'.) Equation 116 is similar to the one given by Boothby,[1] who derived his expression by a different method. The value of microfarads obtained from equation 116 may be called *maximum-torque microfarads*. Almost without exception, this value of microfarads is too great for a practical capacitor-start motor.

A much better value of microfarads is that which gives the maximum starting torque per ampere, as indicated by point C in Figure 4. In Appendix III it is shown that the starting torque per ampere is a maximum when

$$X_{ac}' = \frac{X_m'R_{ac} - Z_m\sqrt{R_{ac}(R_m + R_{ac})}}{R_m} \qquad \begin{matrix}(118)\\(36)\end{matrix}$$

Thus, the procedure of finding the number of microfarads that will yield the maximum starting torque per ampere of line current when the main winding is fixed, is as follows:

(*a*). Compute the algebraic value of X_{ac}' from equation 118. The result will be a negative quantity,

(*b*). Compute the external capacitive reactance from equation 117.

(*c*). $\text{Microfarads} = \dfrac{10^6}{2\pi f(-X_c)}$

(*d*). If the value of microfarads so found is such that its effective resistance, R_c, is considerably different from the assumed value of R_c, steps (*a*), (*b*), and (*c*) can be repeated using the correct value of R_c to obtain R_{ac}. This refinement generally is unnecessary, however.

Calculation of microfarads for maximum starting torque per ampere using the form of Figure 6 can be simplified by rewriting equation 118 in the following form:

---

* Some time after these analyses were made, it was discovered that W. J. Morrill had shown a similar diagram as Figure 4 of his paper,[3] though he did not develop it to the extent done in this paper.

$$X_m'R_{ac}-R_mX_{ac}'=Z_m\sqrt{R_{ac}R_{mac}} \qquad \text{(118a}$$

Equations are developed in Appendix III for R_{ac} and X_{ac}' in terms of the constants of the main winding and the locked-rotor line current. These expressions are

$$X_{ac}'=\frac{X_m'-Z_m(I_L/I_m)}{\left(\frac{I_L}{I_m}\right)^2-1} \qquad \begin{matrix}(120)\\(40)\end{matrix}$$

$$R_{ac}=\frac{R_m}{\left(\frac{I_L}{I_m}\right)^2-1} \qquad \begin{matrix}(121)\\(34)\end{matrix}$$

These equations are similar to 110 and 111 for split-phase motors. However, the author has not found them so useful as their companion formulas for split-phase motors. One reason is that usually it is practicable and more advantageous to use a lower value of R_{ac} than equation 121 indicates. More important, though, is the fact that neither equation 120 nor 121 gives any clue as to the proper number of turns to use in the auxiliary winding. Hence these two equations are of an interest more academic than practical.

Best Winding for a Capacitor-Start Motor

Selection of an auxiliary winding and capacitor for a capacitor-start motor requires the exercise of judgment and experience that cannot be expressed wholly in the form of equations. Attention must be paid to the voltage across the capacitor, not only for the locked-rotor condition, but also for all speeds up to switch-operating speed. Usually it will be necessary to make a number of trial designs before selecting the best one to meet the conditions specified. One procedure is to assume a starting winding, compute the microfarads for maximum starting torque per ampere from equation 118, then with this value of capacitance calculate starting torque, line amperes, and capacitor volts. If the starting torque is low, it may be necessary to do some combination of the following:

1. Increase the number of microfarads.
2. Increase the resistance of the rotor.
3. Increase the number of turns in the auxiliary winding.

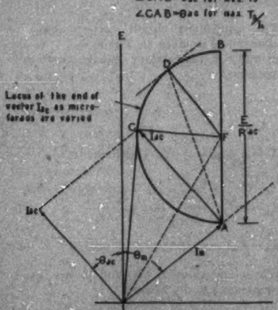
∠DAB = θac for max. Ts
∠CAB = θac for max. Ts/IL
E
B
D
Locus of the end of vector Iac as microfarads are varied
C
Iac
F
E/Rac
Iac
A
θac
θm
Im
O

Figure 4. Starting-torque diagram for a capacitor-start motor

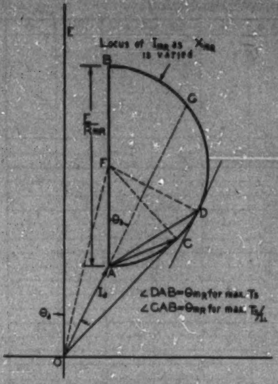

Figure 5. Starting-torque diagram for a reactor-start motor

If the capacitor voltage is high, it may be necessary to do some combination of the following:

1. Increase the microfarads.
2. Increase the resistance of the auxiliary winding.
3. Increase the rotor resistance.

If the locked-rotor current is high, it may be necessary to do one or the other of the following:

1. Increase the rotor resistance.
2. Increase the number of turns in the auxiliary winding and decrease the number of microfarads.
3. Reduce the breakdown torque by increasing the turns of the main winding.

Use of the microfarads for maximum starting torque from equation 116, as shown by point *D* in Figure 4, may result in some of the aforementioned difficulties; more microfarads almost certainly will give trouble. It is best, usually, to work between points *C* and *D* in Figure 4, which means that the value of capacitance chosen should lie between the values given by equations 116 and 118.

Reactor-Start Motors

Reactor-start motors are generally similar to split-phase motors except that, during the starting period, an external reactor is connected in series with the main winding. A circle diagram of the starting conditions of such a motor is given in Figure 5. In Appendix IV, it is shown that the external reactance for maximum starting torque is

$$X_R=\frac{R_aR_{mR}}{Z_a-X_a'}-X_m' \text{ or } \frac{R_{mR}}{R_a}\times(Z_a+X_a')-X_m' \qquad \begin{matrix}(122)\\(44)\end{matrix}$$

Likewise it is shown that the external reactance for maximum starting torque per ampere is

$$X_R=\frac{X_a'R_{mR}+Z_a\sqrt{R_{mR}(R_a+R_{mR})}}{R_a}-X_m' \qquad \begin{matrix}(123)\\(50)\end{matrix}$$

Locked-Rotor Line Current for Motor With Auxiliary Winding

If two impedances, $\dot{Z}_m$ and $\dot{Z}_a$ are connected in parallel to a voltage *E*, the resultant line current, by Kirchhoff's laws, is

$$I_L=\frac{E(\dot{Z}_m+\dot{Z}_a)}{\dot{Z}_m\dot{Z}_a}$$

$$=I_m\frac{(\dot{Z}_m+\dot{Z}_a)}{\dot{Z}_a}=I_m\frac{\dot{Z}_{ma}}{\dot{Z}_a} \qquad (124)$$

or

$$I_L=I_m\frac{Z_{ma}}{Z_a} \qquad (125)$$

where

$$Z_{ma}=\sqrt{(R_m+R_a)^2+(X_m+X_a)^2} \qquad (126)$$

Equation 125 is a most convenient expression for calculating the line current. It appears in modified forms in equations 19, 35, and 48.

A Practical Calculating Form

For determination of the best auxiliary windings and best winding and capacitor combinations, the calculating form shown in Figure 6 was devised with the assistance of H. E. Ellis; it has been of enormous practical value. It was designed for both split-phase and for capacitor-start motors. When all the constants of both windings (and external capacitor, if any) are known, the calculations are made in the order listed except that three

STARTING PERFORMANCE CALCULATIONS

CALCULATION *			1	2	3	4	5	6	7	8
⑳ = EITHER SIN ⑯ OR $\frac{⑲}{⑤ \times ⑬}$										
	RESISTANCE	CAPACITOR								
1	r_{1m}	r_{1m}	1.47	1.47	1.47					
2	r_{2m}'	r_{2m}'	2.67	2.67	2.67					
3	R_m	R_m	4.14	4.14	4.14					
4	X_m'	X_m'	3.72	3.72	3.72					
5	Z_m	Z_m	5.57	5.57	5.57					
6	—	R_c	0.93	0.93	0.93					
7	r_{1a}	r_{1a}	4.83	4.83	4.83					
8	r_{2a}'	r_{2a}'	3.67	3.67	3.67					
9	R_a	R_{ac}	9.43	9.43	9.43					
10	—	$-X_{ac}'$	6.48	6.71	-4.21					
11	X_a'	X_a'	5.14	5.14	5.14					
12	—	$-X_c$	11.62	11.85	9.35					
13	Z_a	Z_{ac}	11.42	11.56	10.31					
14	θ_m	θ_m	42.0		42.0					
15	θ_a	$-\theta_{ac}$	34.5		24.1					
16	⑭ - ⑮	⑭ + ⑮	76.5		66.1					
17	$X_m'R_a$	$X_m'R_{ac}$		35.1						
18	R_mX_a'	$-R_mX_{ac}'$		27.8						
19	⑰ - ⑱	⑰ + ⑱		62.9						
20	SIN $(\theta_m-\theta_a)$	SIN $(\theta_m-\theta_{ac})$	0.972	0.977	0.915					
21	×K	×K	1.172	1.172	1.172					
22	×1.88p/f	×1.88p/f	0.1252	0.1252	0.1252					
23	$\times r_{2m}'$	$\times r_{2m}'$	2.67	2.67	2.67					
24	$\times I_m$	$\times I_m$	20.6	20.6	20.6					
25	$\times I_a$	$\times I_{ac}$	10.05	9.96	11.16					
26	$=T_s$	$=T_s$	78.7	78.5	82.5					
27	R_{ma}	R_{mac}	13.57	13.57	13.57					
28	X_{ma}	X_{mac}	2.76	2.99	0.49					
29	Z_{ma}	Z_{mac}	13.84	13.89	13.57					
30	$I_L=I_m Z_{ma}/Z_a$	$I_L=I_m Z_{mac}/Z_{ac}$	25.0	24.8	27.1					
31	CM/I_a	$E_c=I_{ac}X_c$	116.9	118.0	104.2					
32		$VA_c=I_{ac}^2X_c$								
33		MFD	226	224	284					
34		T_s/I_L	3.15	3.16	3.04					

1/3 HP 115 VOLTS 60 CYCLES 4 POLES CALC. REF. ______ DATE 11-5-43

TYPE CAP START SIGNATURE CGVeinott L- ______

Figure 6. Form for calculating starting performance

items may be omitted; these items are 14 to 16, inclusive, or 17 to 19, inclusive, depending upon the preference of the calculator. Such a set of calculations is shown in column 1.

For calculating maximum-torque-per-ampere windings for capacitor-start motors, the following procedure has been found very useful and helpful. First, the known constants of the two windings are entered in lines 1, 2, 4, 7, 8, 11, 21, 22, 23. A value of R_c for line 6 is assumed and entered. The following lines are then computed in the order named: 3, 5, 9, 17, 27. Line 19 is then computed by making use of equation 118a. Then, the remaining items are computed and filled in in this order: 18, 10, 12, 13, 20, 24 to 33, inclusive. This procedure yields the microfarads for maximum starting torque per ampere, starting torque obtained, locked-rotor currents in each winding and in the line, and capacitor volts and volt-amperes. Whereas X_{as}' may be computed from equation 118 and the items of Figure 6 calculated in consecutive order, the procedure outlined reduces considerably the numerical computations involved. Such a set of calculations is made in column 2. It is seen that the capacitor for column 1 was already chosen so as to give practically maximum starting torque per ampere. As a comparison, X_{ac} for maximum starting torque was computed, and complete calculations made as listed in column 3.

Maximum-torque-per-ampere windings for split-phase motors can be computed by a similar procedure, using equation 109a, but usually it is best to determine these windings from equations 110 and 111, or 110 and 115.

Symbols (See, Also, Table I)

r_1 = primary resistance

r_2 = secondary resistance, referred to primary

$C_r = \dfrac{\text{apparent rotor resistance}}{\text{actual rotor resistance}}$ defined by equation 9

$C_R = \dfrac{\text{apparent total reactance}}{\text{ideal total reactance}}$ defined by equation 10

X = ideal short-circuit reactance. This is the short-circuit reactance the motor would have if the secondary resistance were zero

X_0 = open-circuit reactance

T_s = starting torque, in ounce-feet

I_L = total locked-rotor current

P = number of poles

K = winding ratio = $\dfrac{\text{number of effective conductors in auxiliary winding}}{\text{number of effective conductors in main winding}}$

f = frequency in cycles per second

$\dot{Z} = R + jX$

$Z = \sqrt{R^2+X^2}$

Appendix I

Locked-Rotor Impedance of Main Winding of a Single-Phase Induction Motor

In Figure 1 is shown the equivalent circuit of a single-phase induction motor under locked-rotor conditions. From inspection of this figure, the apparent impedance of the motor is

$$\dot{Z} = r_1 + jx_1 + \frac{(jx_m)(r_2+jx_2)}{r_2+j(x_m+x_2)} \tag{1}$$

which can be reduced, by ordinary algebraic processes, to the form

$$\dot{Z} = (r_1 + r_2C_r) + (jXC_R) \tag{2}$$

where

$$C_r = \frac{x_m^2}{(x_2+x_m)^2+r_2^2} \tag{3}$$

$$C_R = \frac{1+\left(\frac{x_1+x_m}{X}\right)\left(\frac{r_2}{x_2+x_m}\right)^2}{1+\left(\frac{r_2}{x_2+X_m}\right)^2} \tag{4}$$

X = "ideal short-circuit reactance"

$$= x_1 + \frac{x_2x_m}{x_2+x_m} \tag{5}$$

The equivalent circuit corresponding to equation 2 is given in Figure 1C. If the primary and secondary leakage reactances are equal

$x_1 = x_2$

Let

$$X_0 = x_1 + x_m = x_2 + x_m = \text{open-circuit reactance} \tag{6}$$

$$K_p = \frac{x_m}{x_m+x_1} = \frac{x_m}{x_m+x_2} = \sqrt{\frac{X_0-X}{X_0}} \tag{7}$$

$$K_r = K_p^2 \tag{8}$$

When equations 6, 7, and 8 are used, equations 3 and 4 become

$$C_r = \frac{K_r}{1+(r_2/X_0)^2} = \frac{\text{apparent rotor resistance}}{\text{actual rotor resistance}} \tag{9}$$

$$C_R = \frac{1+\frac{r_2}{X}\frac{r_2}{X_0}}{1+(r_2/X_0)^2} = \frac{\text{apparent total reactance}}{\text{ideal total reactance}} \qquad (10)$$

Equations 9 and 10, when substituted into equation 2, give the identical results reported in equations 1, 2, and 3 of a previous paper,[3] wherein it was pointed out that the same equations were obtained by both the revolving-field and by the cross-field method of analysis.

Values of C_r and C_R have been plotted on Figure 2 to show the range of variation that may be expected in any given motor. These curves show that, for most practical purposes, C_r may be taken as equal to K_r, whereas C_R cannot be neglected, especially if the rotor resistance is even moderately high. Let

$r_2' = r_2C_r$ = apparent secondary resistance (11)

$X' = XC_R$ = apparent total reactance (12)

Then equation 2 becomes

$$\hat{Z} = (r_1+r_2')+jX'$$

It should be noted in passing that Figure 1B also applies to a polyphase induction motor; hence all equations in this appendix apply also to a polyphase induction motor.

Appendix II

Maximum Starting Torque Per Ampere for Split-Phase Motors

Consider point C in Figure 3, which defines the maximum starting torque per ampere. The first problem is to express the line amperes, I_L, in terms of constants already known

$$\overline{OC}^2 = \overline{OF}^2 - \overline{CF}^2 = \overline{OF}^2 - \overline{AF}^2 \qquad (14)$$

(Line $\overline{OF}$ is omitted from Figure 3 to avoid confusion.)

But

$$\overline{OF}^2 = \overline{OA}^2 + \overline{AF}^2 + 2\overline{OA}\ \overline{AF}\sin\theta_m \qquad (15)$$

Therefore

$$\overline{OC}^2 = \overline{OA}^2 + 2\overline{OA}\ \overline{AF}\sin\theta_m$$

$$= I_m^2 + \frac{2I_mEX_m'}{2X_a'Z_m}$$

$$= I_m^2\left(1+\frac{X_m'}{X_a'}\right) \qquad (16)$$

The line current is then

$$I_L = I_m\sqrt{\left(1+\frac{X_m'}{X_a'}\right)} \qquad (17)$$

Solving equation 17 for X_a' gives

$$X_a' = \frac{X_m'}{\left(\frac{I_L}{I_m}\right)^2-1} \qquad (18)$$

But

$$I_L = I_m\sqrt{\frac{(R_m+R_a)^2+(X_m'+X_a')^2}{(R_a^2+X_a'^2)}}$$

[Refer to equation 125] (19)

Eliminating I_L between equations 17 and 19 and solving for R_a gives

$$R_a = \frac{R_mX_a'+Z_m\sqrt{X_a'(X_m'+X_a')}}{X_m'} \qquad (20)$$

$$\frac{R_a}{X_a'} = \frac{R_m}{X_m'} + \frac{Z_m}{X_m'}\sqrt{\frac{X_m'+X_a'}{X_a'}} \qquad (21)$$

but, from equation 17

$$\sqrt{\frac{X_m'+X_a'}{X_a'}} = I_L/I_m \qquad (22)$$

so, equation 21 becomes

$$\frac{R_a}{X_a'} = \frac{R_m+Z_m(I_L/I_m)}{X_m'} \qquad (23)$$

or

$$R_a = \frac{X_a'}{X_m'}(R_m+Z_mI_L/I_m) \qquad (24)$$

Substituting the value of X_a' in equation 18 for X_a' in equation 24 yields

$$R_a = \frac{R_m+Z_m(I_L/I_m)}{\left(\frac{I_L}{I_m}\right)^2-1} \qquad (25)$$

Equations 25 and 18 define the resistance and reactance in the auxiliary-winding circuit, which yields a line current of I_L and a maximum-torque-per-ampere winding. These two equations completely define the auxiliary winding. If the reactance of the auxiliary winding is known, equation 20 defines the resistance required and equation 17 the resultant line current.

Appendix III

Starting-Torque Equations for a Capacitor-Start Motor

CONDITIONS FOR MAXIMUM STARTING TORQUE

Starting conditions for a capacitor-start motor are illustrated in Figure 4. Points C and D represent maximum starting torque per ampere, and maximum starting torque, respectively.

By the geometry of Figure 4

$$\overset{\frown}{AD} = \frac{180°+2\theta_m}{2} = 90°+\theta_m$$

$$\overset{\frown}{DB} = 180° - \overset{\frown}{AD} = 90-\theta_m \qquad (26)$$

or

$$\theta_{ac} = \frac{90-\theta_m}{2}$$

$$\tan(-\theta_{ac}) = \sqrt{\frac{1-\cos(90-\theta_m)}{1+\cos(90-\theta_m)}}$$

$$= \sqrt{\frac{1-\sin\theta_m}{1+\sin\theta_m}} = \sqrt{\frac{Z_m-X_m'}{Z_m+X_m'}}$$

or

$$\frac{-X_{ac}'}{R_{ac}} = \frac{Z_m-X_m'}{R_m} = \frac{R_m}{Z+X_m'}$$

or

$$-X_{ac}' = \frac{R_{ac}}{R_m}(Z_m-X_m') = \frac{R_{ac}R_m}{Z+X_m'} \qquad (28)$$

Equation 28 expresses the total net reactance required in the auxiliary phase for maximum starting torque. This equation checks one given by Boothby,[1] if allowance is made for the difference in notation. The quantities given are all algebraic, not vector, quantities.

CONDITIONS FOR MAXIMUM STARTING TORQUE PER AMPERE

Maximum starting torque per ampere is represented by point C in Figure 4. The line current, OC, is expressed by the relationship

$$\overline{OC}^2 = \overline{OF}^2 - \overline{CF}^2 = \overline{OF}^2 - \overline{AF}^2 \qquad (29)$$

but

$$\overline{OF}^2 = \overline{OA}^2 + \overline{AF}^2 + 2\overline{OA}\ \overline{AF}\cos\theta_m \qquad (30)$$

therefore

$$\overline{OC}^2 = \overline{OA}^2 + 2\overline{OA}\ \overline{AF}\cos\theta_m \qquad (31)$$

$$I_L^2 = I_m^2 + \frac{2I_m}{2}\frac{E}{R_{ac}}\times\frac{R_m}{Z_m} \qquad (32)$$

$$I_L = I_m\sqrt{\frac{R_{ac}+R_m}{R_{ac}}} \qquad (33)$$

or

$$R_{ac} = \frac{R_m}{(I_L/I_m)^2-1} \qquad (34)$$

This gives an expression for the total resistance in the auxiliary phase. The next step is to find a similar expression for the total reactance in the auxiliary phase. The general expression for line current for a capacitor-start motor is

$$I_L = I_m\frac{\sqrt{(R_m+R_{ac})^2+(X_m'+X_{ac}')^2}}{Z_{ac}}$$

(refer to equation 125) (35)

Eliminating I_L by combining equations 33 and 35, and solving for X_{ac}', one obtains

$$X_{ac}' = \frac{X_m'R_{ac}-Z_m\sqrt{R_{ac}(R_m+R_{ac})}}{R_m} \qquad (36)$$

Remembering that

$$X_c = X_{ac}' - X_a' \qquad (37)$$

equation 36 can be rewritten

$$X_c = \frac{X_m'R_{ac}-X_a'R_m-Z_m\sqrt{R_{ac}(R_m+R_{ac})}}{R_m} \qquad (38)$$

Equation 38 gives the value of external capacitive reactance to be connected in series with the auxiliary winding to obtain maximum starting torque per ampere.

We still have to find X_{ac}' for maximum starting torque per ampere in terms of the line current. Dividing numerator and denominator of the right member of equation 36 by R_{ac}

$$X_{ac}' = \frac{X_m'-Z_m\sqrt{\frac{R_m+R_{ac}}{R_{ac}}}}{R_m/R_{ac}} \qquad (39)$$

By use of equation 33, equation 39 reduces to

$$\frac{X_m'-Z_m(I_L/I_m)}{\left(\frac{I_L}{I_m}\right)^2-1} \tag{40}$$

Equations 40 and 34 give the total reactance and total resistance required in the auxiliary phase for a maximum-torque-per-ampere winding having a locked-rotor current of I_L.

Appendix IV

Starting-Torque Equations for a Reactor-Start Motor

The starting conditions of a reactor-start motor are represented by Figure 5. Points C and D represent maximum starting torque per ampere and maximum starting torque, respectively.

MAXIMUM STARTING TORQUE

$$\widehat{AD}=\frac{180°-2\theta_a}{2}=90°-\theta_a \tag{41}$$

$$\theta_{mR}=\angle DAB=\frac{\widehat{BD}}{2}=\frac{90°+\theta_a}{2} \tag{42}$$

By comparing equation 42 with the derivation of equation 28 in Appendix III, it is seen that

$$X_{mR}=\frac{R_aR_{mR}}{Z_a-X_a'} \quad \text{or} \quad \frac{R_{mR}}{R_a}(Z_a+X_a') \tag{43}$$

since

$$X_R=X_{mR}-X_m'$$

$$X_R=\frac{R_aR_{mR}}{Z_a-X_a'}-X_m' \text{ or } \frac{R_{mR}}{R_a}(Z_a+X_a')-X_m' \tag{44}$$

Equation 44 gives the value of external reactance to be inserted in series with the main winding to obtain maximum starting torque.

MAXIMUM STARTING TORQUE PER AMPERE

In Figure 5, wherein point C represents maximum starting torque per ampere,

$$\overline{OC}^2=\overline{OF}^2-\overline{CF}^2=\overline{OF}^2-\overline{AF}^2 \tag{45}$$

but

$$\overline{OF}^2=I_a^2+\overline{AF}^2+2I_a\overline{AF}\cos\theta_a$$

or

$$\overline{OC}^2=I_a^2+\frac{2I_aE}{2R_{mR}}\times\frac{R_a}{Z_a}=I_L^2 \tag{46}$$

or

$$I_L=I_a\sqrt{\frac{R_{mR}+R_a}{R_{mR}}} \tag{47}$$

The line current, I_L, is also expressed by the relation

$$I_L=I_a\frac{\sqrt{(R_{mR}+R_a)^2+(X_{mR}+X_a)^2}}{Z_{mR}}$$

[refer to equation 125] (48)

Equating the right-hand members of equations 47 and 48 and solving for X_{mR} yields

$$X_{mR}=\frac{X_a'R_{mR}+Z_a\sqrt{R_{mR}(R_a+R_{mR})}}{R_a} \tag{49}$$

The total external reactance to be connected in series with the main winding to obtain maximum starting torque per ampere is, therefore

$$X_R=\frac{X_a'R_{mR}+Z_a\sqrt{R_{mR}(R_a+R_{mR})}}{R_a}-X_m' \tag{50}$$

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Crossed-Coil Power-Factor Meters

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Synopsis: The fundamental relations governing operation of the crossed-coil type of power-factor meter are derived for the particular measurement applications on single-phase, three-wire two-phase, three-wire three-phase, and four-wire three-phase circuits. Emphasis is placed on the usefulness of a graph of either the equilibrium equation or the torque relation in determining the scale distribution.

DURING the past decade or so, the continued growth and, in recent years, the rapid expansion of distribution systems in industry has made industry more and more conscious of the importance of power factor. Although it is recognized that the electrical industry is gradually abandoning the concept of power factor as an indication of load conditions and is concentrating more on measurement of watts and vars, there still are many applications where the measurement of power factor is essential.

The increased demand for the measurement of power factor on single-phase and polyphase circuits has made it necessary to design instruments with a large number of scale ranges. The range of the scale and the application of the instrument to a specified circuit determines the scale distribution. The factors which control scale distribution can be shown from the fundamental relations. To do this it will be necessary

1. To derive the basic torque and equilibrium equations for the single-phase, three-wire two-phase, three-wire three-phase, and four-wire three-phase power-factor meters.
2. To show how the scale distribution can be determined completely from a graph of either the equilibrium expression or the torque equations.

Examples will be made emphasizing the usefulness of these graphs for the selection of scale ranges for given scale angles of deflection.

It is realized that there are several ways by which power factor can be measured. The company with which the author is associated makes two types of power-factor meters. One uses a polarized Z-shaped vane which operates in a rotating magnetic field, and the other makes use of the principle of the crossed coil. In this article attention will be directed to the fundamentals of the crossed-coil-type power-factor meter.

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Figure 1. Type AD-6 power-factor meter

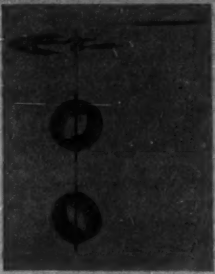

Figure 1a. Moving element of a power-factor meter, showing arrangement of crossed coils

Power-factor meters of the crossed-coil type similar to that shown in Figure 1 are adaptations of the original Tuma phase meter. (See page 546 of reference 1.) The Tuma phase meter as originally developed is a single-phase instrument for measuring the angle between two voltages or a voltage and a current. Although the three-wire three-phase application of the Tuma phase meter is more familiar because of use, it will be well to consider the single-phase application first because of its relative simplicity in analysis by comparison.

Single-Phase Power-Factor Meter: Scale Lag–Lead

The derivation of the fundamental relation is adequately covered in the literature for the single-phase meter. However, it will be advantageous to review this derivation in order to outline the method of attack that will be used in the analysis of the polyphase power-factor-meter applications. Figure 2 is a schematic diagram of the essential elements of a single-phase power-factor meter. Coils *A* and *B* compose the moving element. These coils are rigidly coupled to a shaft which is pivoted in an essentially uniform magnetic field produced by field coil *F*. An inductance in series with coil *B* and a resistance in series with coil *A* is connected to the external circuit as shown in the diagram. When current passes through coils *F*, *A*, and *B*, fields will be set up, and coils *A* and *B* will each experience turning moments in opposite directions. As *A* and *B* are rigidly connected, the moving element will turn to such a position that the resultant moment acting on it will be zero. The deflection *D* will then be an indication of the power-factor angle or phase angle between the load current and the line voltage.

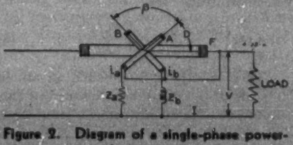

Figure 2. Diagram of a single-phase power-factor meter

Referring to Figure 2, let

D = Angle of deflection as measured between the planes of coils *F* and *A*
β = Angle between coils *A* and *B*
θ = Power-factor angle or angle between the load current *I* and the line voltage *V*
Δ = Angle of lag between current in coil *B* and voltage *V*
α = Angle of lag between the current in coil *A* and voltage *V*
Z_a, Z_b = Impedances of circuits *A* and *B* containing coils *A* and *B*, respectively
i_a = Instantaneous current in circuit *A*
i_b = Instantaneous current in circuit *B*
i = Instantaneous load current

There will be some mutual inductance between the moving and fixed coils, but in most applications the effect will be negligibly small. In the derivation of the fundamental relations the effect of mutual inductance will be neglected.

If voltage *V* is taken as the reference vector from which phase differences are measured and the load current *I* is sinusoidal and is lagging, the instantaneous current values are

$$i = I \sin (wt - \theta)$$

$$i_a = \frac{V}{Z_a} \sin (wt - \alpha)$$

$$i_b = \frac{V}{Z_b} \sin (wt - \Delta)$$

The instantaneous torque acting on coil *A* will be

$$\tau_a = K_a i_a i \sin D$$

or

$$\tau_a = \frac{K_a I V}{Z_a} \sin D \sin (wt - \theta) \sin (wt - \alpha)$$

and the average torque over one complete period is

$$\mathrm{T}_a = \left[\frac{K_a I V}{Z_a}\right] \sin D \frac{1}{T} \int_0^T \sin (wt - \theta) \times \sin (wt - \alpha) dt$$

where T is the period and is equal to the reciprocal of the frequency *f*. Frequency will be assumed constant in the derivation. K_a is a constant involving coil configuration, turns, and so forth.

If the preceding expression is integrated, the final average torque for coil *A* becomes

$$\mathrm{T}_a = 1/2 \left[\frac{K_a I V}{Z_a}\right] \sin D \cos (\theta - \alpha) \qquad (1)$$

Similarly, the instantaneous torque acting on coil *B* is

$$\tau_b = K_b i_b i \sin (D + \beta)$$

or

$$\tau_b = \left[\frac{K_b I V}{Z_b}\right] \sin (D + \beta) \sin (wt - \theta) \times \sin (wt - \Delta)$$

and the average torque over a period is

$$\mathrm{T}_b = \left[\frac{K_b I V}{Z_b}\right] \sin (D + \beta) \frac{1}{T} \int_0^T \sin (wt - \theta) \times \sin (wt - \Delta) dt$$

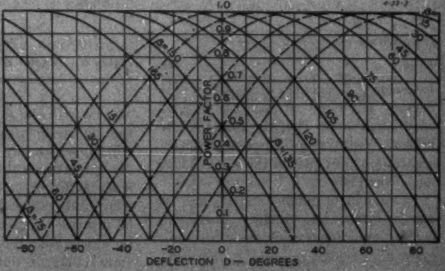

Figure 3. Scale distribution curves of a single-phase power-factor meter for scale lag–lead, plotted from the equilibrium equation

$\beta = \Delta$

—— Curves of β for lagging power factor

--- Curves of β for leading power factor

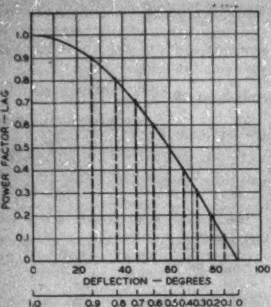

Figure 4. Scale distribution of a single-phase power-factor meter, for condition of $\beta=\Delta=90$ degrees

Therefore

$$T_b=1/2\left[\frac{K_bIV}{Z_b}\right]\sin(D+\beta)\cos(\theta-\Delta)\qquad(2)$$

Now for equilibrium the two torques must be equal

$$T_a=T_b\qquad(3)$$

or

$$1/2\left[\frac{K_aIV}{Z_a}\right]\sin D\cos(\theta-\alpha)$$

$$=1/2\left[\frac{K_bIV}{Z_b}\right]\sin(D+\beta)\cos(\theta-\Delta)\,(4)$$

The foregoing is the general expression for equilibrium, and if the design of the instrument is such that

$$\frac{K_a}{Z_a}=\frac{K_b}{Z_b}\qquad(5)$$

then equation 4 reduces to

$$\sin D\cos(\theta-\alpha)=\sin(D+\beta)\cos(\theta-\Delta)\quad(6)$$

In the ideal instrument $\alpha=0$, $\beta=90$ degrees and $\Delta=90$ degrees. For these conditions equation 6 reduces to

$$\text{Sin } D\cos\theta=\sin(D+90)\cos(\theta-90)$$

or

$$\text{Sin } D\cos\theta=\cos D\sin\theta$$

and

$$\text{Tan } D=\tan\theta$$

from which

$$D=\theta\qquad(6a)$$

In practice Δ is never exactly equal to 90 degrees, but Laws on page 548 of reference 1 has shown that the change in deflection from the unity-power-factor reference point is equal to the power-factor angle when $\beta=\Delta$.

If we had originally stated that the power factor was leading instead of lagging, equation 6 would have become

$$\text{Sin } D\cos(\theta-\alpha)=\sin(D+\beta)\cos(\theta+\Delta)\,(6b)$$

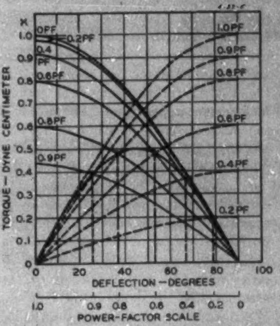

Figure 5. Scale distribution of a single-phase power-factor meter, plotted from the dynamic torque curves

$\beta=\Delta=90$ degrees
--- Coil *A*
—— Coil *B*

and for the ideal instrument the deflection

$$D=-\theta\qquad(6c)$$

Thus we see that for lagging and leading power factors the power-factor angle is equal to plus and minus values of the deflection angle D, respectively.

Assuming in equation 6 that α is negligibly small and solving for D, we find that

$$\text{Cot } D=\frac{1}{\sin\beta(\cos\Delta+\tan\theta\sin\Delta)}-\cot\beta\ (7)$$

Figure 3 is a plot of function 7 where β has been set equal to all assigned values of Δ. The full lines represent the curves of β for lagging power factor, whereas the dotted lines are the β curves for leading power factor.

If we arbitrarily let the deflection D be positive for lagging power-factor indications and let the instrument pointer be aligned with the *A* coil, we are in a position to predict from these curves what will happen for any particular value of the angle β between the coils. For instance, if $\beta=90$ degrees and it is desired to have a lag–lead scale with a total scale angle of 90 degrees, the power-factor scale would be approximately 0.7–1.0–0.7. This assumes that at unity power factor the pointer indicates midscale and the deflection D must be ±45 degrees.

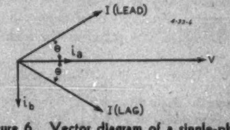

Figure 6. Vector diagram of a single-phase power-factor meter for scale lag–lead

Again suppose $\beta=60$ degrees the scale angle is still 90 degrees and the pointer aligned with coil *A*. At unity power factor the pointer will indicate 30 degrees from mid scale on the lag side. Consequently, if it was desired to keep the mid-scale point the unity-power-factor point, it would be necessary to advance the pointer 30 degrees on the shaft. In the conventional design of instrument this may not be mechanically feasible, but if it were feasible, the scale distribution would be identical to that obtained for $\beta=90$ degrees. From this family of curves it is evident that if the angle β between the coils is always adjusted so that it equals Δ, the power-factor scales will have identical distributions.

Figure 4 shows the normal distribution of an instrument for the condition where $\beta=\Delta=90$ degrees. One obtains this distribution by projecting the power-factor points from the curve to the abscissa and labeling such points in terms of power factor instead of deflection. The resulting distribution constitutes the power-factor scale.

The scale distribution can also be determined completely from a plot of the dynamic torque curves of each moving coil. If the torque expressions 1 and 2 for coils *A* and *B* are plotted, for values of power factor, the points of intersection of corresponding torque curves will form a locus of points of equilibrium. The points on this locus when projected to the abscissa will uniquely determine the scale distribution.

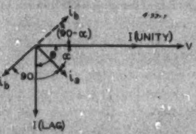

Figure 7. Vector diagram of a single-phase power-factor meter for scale all lag

The dynamic torque curves are also valuable in that they describe how torque varies with angular deflection for each coil. Figure 5 shows a graph of these torque curves where for simplicity we have arbitrarily set

$$K=1/2\left[\frac{K_aIV}{Z_a}\right]=1/2\left[\frac{K_bIV}{Z_b}\right]\qquad(8)$$

Theoretically it is possible to obtain a scale of 0–1.0–0 power factor, but this would require an instrument scale angle of 180 degrees. In the types *AD*-6 and 7 power-factor meters, the maximum

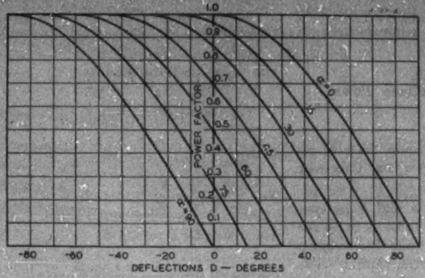

Figure 8. Scale distribution curves of a single-phase power-factor meter for scale all lag, plotted from the equilibrium equation

$\beta=90$ degrees

scale for lag and lead is 0.5–1.0–0.5 power factor. This is obtained with $\beta=\Delta=90$ degrees and the pointer aligned with coil *A*.

Occasionally it is required to produce a scale all lag or all lead. In the foregoing analysis we have seen that theoretically it is possible to obtain a scale all lag or all lead by mechanically shifting the pointer with respect to coils *A* and *B*. Mechanical design may make it virtually impossible to obtain the proper pointer-coil relation to give a scale all lag or lead. Consequently, it becomes necessary to make certain changes in the electrical constants. The method for doing this is discussed in the following section.

Single-Phase Power-Factor Meter: Scale All Lag

In the following analysis let us assume a condition in which the scale is all lag, that is, unity power factor is at the right end of the scale and zero power factor at the left end. This analysis can also apply to any such scale range as 0.3 to 0.8 power-factor lag. To do this and still keep the same mechanical arrangement of coils and pointer as in the previous section it is necessary to shift electrically the 90-degree relation of the potential circuits such that the instrument indication will fall within the prescribed scale angle.

In the standard lag–lead instrument of the previous section the current i_a in circuit *A* is in phase with the applied voltage and i_b is 90 degrees lagging this voltage. Vectorially, the voltage *V* and i_a bisects the angle of the lead and lag load-current vectors which represent the extremes of the power-factor scale range. Figure 6 shows this vector relation. If the scale is to be all lag, then the current i_a must be made to lag *V* by some angle α such that the current vector i_a will approximately bisect the angle enclosed by the load-current vectors representing the ends of the power-factor scale. Current i_b must also lag *V* by $(\alpha+90)$. To obtain the proper lag relation between i_b and *V* capacitance and resistance are placed in series with coil *B*. Current i_a will now lead *V* by $(90-\alpha)$ but can be made to lag by reversing the connections. Figure 7 shows the new vector relations. By referring to the vector diagram in Figure 7 the instantaneous current values can now be determined and the torque expressions derived. The instantaneous currents are

$$i=I\sin(wt-\theta)$$

$$i_a=\frac{V}{\sqrt{R_a^2+L_a^2\omega^2}}\sin(wt-\alpha)$$

$$i_b=\frac{V}{\sqrt{R_b^2+1/C_b^2\omega^2}}\sin[wt+(90-\alpha)]$$

Following the previous procedure the torque on coil *A* is

$$\mathrm{T}_a=\left[\frac{K_aIV}{\sqrt{R_a^2+L_a^2\omega^2}}\right]\sin D\frac{1}{T}\times\int_0^T\sin(wt-\theta)\sin(wt-\alpha)dt$$

which upon integration yields

$$\mathrm{T}_a=1/2\left[\frac{K_aIV}{\sqrt{R_a^2+L_a^2\omega^2}}\right]\times\sin D\cos(\theta-\alpha)\qquad(9)$$

and the torque on coil *B* is

$$\mathrm{T}_b=\left[\frac{K_bIV}{\sqrt{R_b^2+1/C_b^2\omega^2}}\right]\sin(D+\beta)\frac{1}{T}\times\int_0^T\sin(wt-\theta)\sin[wt+(90-\alpha)]dt$$

or

$$\mathrm{T}_b=1/2\left[\frac{K_bIV}{\sqrt{R_b^2+1/C_b^2\omega^2}}\right]\times\sin(D+\beta)\sin(\theta-\alpha)\qquad(10)$$

For equilibrium

$$\mathrm{T}_a=\mathrm{T}_b$$

When

$$\frac{K_a}{\sqrt{R_a^2+L_a^2\omega^2}}=\frac{K_b}{\sqrt{R_b^2+1/C_b^2\omega^2}}$$

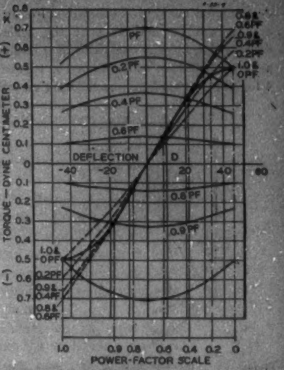

Figure 9. Scale distribution of a single-phase power-factor meter with scale all lag, determined from the dynamic torque curves

$\alpha=45$ degrees
$\beta=90$ degrees
--- Coil *A*
—— Coil *B*

the equilibrium equation becomes

$$\operatorname{Sin}D\cos(\theta-\alpha)=\sin(D+\beta)\sin(\theta-\alpha)\qquad(11)$$

For the case when $\beta=90$, expression 11 becomes

$$\operatorname{Sin}D\cos(\theta-\alpha)=\cos D\sin(\theta-\alpha)$$

or

$$\operatorname{Tan}D=\tan(\theta-\alpha)$$

from whence

$$D=\theta-\alpha\qquad(11a)$$

The deflection is now equal to the difference between θ and α and not equal to θ as previously. However α is a constant, and the deflection *D* is proportional to θ. If equation 11 is solved for *D*

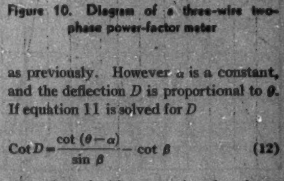

Figure 10. Diagram of a three-wire two-phase power-factor meter

$$\operatorname{Cot}D=\frac{\cot(\theta-\alpha)}{\sin\beta}-\cot\beta\qquad(12)$$

The plot of this expression for $\beta=90$

degrees and varying α is shown on Figure 8. It will be noticed that the scale distribution for any one value of α is the same distribution as developed in Figure 4 for the single-phase power-factor meter

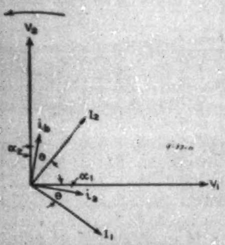

Figure 11. Vector diagram of a three-wire two-phase power-factor meter

with scale lag–lead. In fact, expression 12 for $\alpha=0$ and $\beta=90$ reduces to expression 7 for $\beta=\Delta=90$. From this we could have predicted the distribution would be the same.

The torque curves of each element depicted in Figure 9 are radically different from those in Figure 4, but the resultant scale distribution is the same. Although angle α was arbitrarily set equal to 45 degrees for these torque curves, other values of α would give similar scale distributions. The essential difference for other values of α would be revealed in different values of the angle of deflection.

By referring again to Figure 8 it will be noted that if it is required to have a scale 0.3 to 0.8 power factor or the current lags the voltage by 72.5 and 37 degrees, respectively, α should be chosen to be approximately 60 degrees. As pointed out previously, it is necessary to choose α intermediate between the angles representing the scale ends if the same coil and pointer relation is to be maintained, namely, the pointer lines up with the A coil. If this is done, then the resulting scale angle will be approximately 38 degrees.

Three-Wire Two-Phase Power-Factor Meter

Some applications require the measurement of power factor on three-wire two-phase circuits.

In Figure 10 coils A and B are connected in series with resistors to phases 1 and 2, respectively, while their common lead is connected to the neutral. The same notation shall be used as previously with the exception that α_1 and α_2 are the angles of lag between the currents in circuits A and B and the load voltage. These lagging currents are due to the inductive effect of the movable coils.

With reference to Figure 11 which is the vector diagram for a balanced inductive load the instantaneous current values for lagging power factor are

$$i=I\sin(wt-\theta)$$

$$i_a=\frac{V}{Z_a}\sin(wt-\alpha_1)$$

$$i_b=\frac{V}{Z_b}\sin(wt+[90-\alpha_2])$$

The coil torques are now

$$\mathrm{T}_a=\left[\frac{K_aIV}{Z_a}\right]\sin D\frac{1}{T}\int_0^T\sin(wt-\theta)\times\sin(wt-\alpha_1)dt$$

$$\mathrm{T}_a=1/2\left[\frac{K_aIV}{Z_a}\right]\sin D\cos(\theta-\alpha_1) \qquad (13)$$

$$\mathrm{T}_b=\left[\frac{K_bIV}{Z_b}\right]\sin(D+\beta)\frac{1}{T}\times\int_0^T\sin(wt-\theta)\sin[wt+(90-\alpha_2)]dt$$

$$\mathrm{T}_b=1/2\left[\frac{K_bIV}{Z_b}\right]\sin(D+\beta)\sin(\theta-\alpha_2) \qquad (14)$$

For equilibrium T_a must equal T_b; therefore

$$1/2\left[\frac{K_aIV}{Z_a}\right]\sin D\cos(\theta-\alpha_1)=1/2\left[\frac{K_bIV}{Z_b}\right]\sin(D+\beta)\sin(\theta-\alpha_2) \qquad (15)$$

Letting

$$K_1=\frac{K_a}{Z_a}\text{ and }K_2=\frac{K_b}{Z_b}$$

and solving for D results in

$$\operatorname{Cot}D=\frac{K_1}{K_2}\left[\frac{1+\tan\theta\tan\alpha_1}{\sin\beta\left(\tan\theta\frac{\cos\alpha_2}{\cos\alpha_1}-\frac{\sin\alpha_2}{\cos\alpha_1}\right)}\right]-\cot\beta \qquad (16)$$

If design is such that $K_1=K_2$ then $\alpha_1=\alpha_2=\alpha$ and equation 16 reduces to

$$\operatorname{Cot}D=\frac{\cot(\theta-\alpha)}{\sin\beta}-\cot\beta \qquad (17)$$

This relation is identical to equation 12 for the single-phase power-factor meter

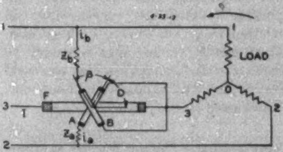

Figure 13. Diagram of a three-wire three-phase power-factor meter

with scale all lag. However, in general α in equation 17 is small and can be neglected. If this is done, the deflection then reduces to

$$\operatorname{Cot}D=\frac{\cot\theta}{\sin\beta}-\cot\beta \qquad (18)$$

The graph of this expression will give the same scale distribution as the single-phase power-factor meter for $\beta=90$ degrees. Equation 7 is identical to equation 18 for $\Delta=90$ degrees. The essential difference between the operation of a power-factor meter on three-wire two-phase and a single-phase meter with scale lag–lead is that in the three-wire two-phase application the 90-degree relation is obtained by the 90-degree displacement between phase voltages, whereas in the single-phase meter it is necessary to lag the current in one circuit by the use of a series reactor.

If expression 18 is plotted for lag and lead power factors with varying β, a family of distribution curves will result as shown on Figure 12. Each value of β

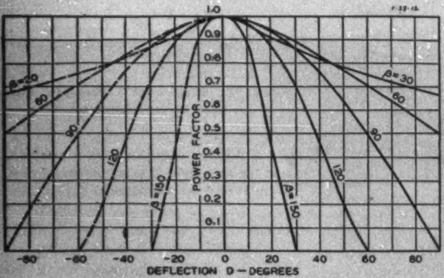

Figure 12. Scale distribution curves for a three-wire two-phase power-factor meter, plotted from the equilibrium equation

—— β curves for lagging power factor

– – – β curves for leading power factor will give a different scale distribution. The curve that will give the same scale distribution as the single-phase application is the one for which $\beta=90$ degrees.

It can be seen from Figure 12 that for a given scale angle of 90 degrees with, for example, $\beta=60$ degrees that the maximum power-factor scale that can be obtained for a scale all lag or all lead would be 0.5 to 1.0 power factor. Also, for a scale having both lag and lead with the same scale angle, the maximum range would be approximately 0.8 lag to 0.8 lead.

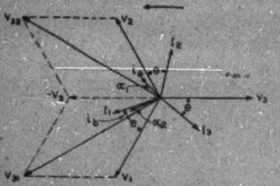

Figure 14. Vector diagram of a three-wire three-phase power-factor meter

It will not be necessary to plot the dynamic torque curves as the curves of Figure 5 with the resulting scale distribution apply to the three-wire two-phase power-factor meter for $\beta=90$ degrees.

Three-Wire Three-Phase Power-Factor Meter

Figure 13 shows the connection of a power-factor meter on a three-wire three-phase balanced inductive load. Again use is made of the phase relation between voltages to obtain the desired condition for power-factor measurement. The only mechanical difference between the three-wire two-phase and three-wire three-phase power-factor meter is the setting of the coil angle β and pointer location. Although the mechanical difference is slight, the torque and distribution characteristics are radically different between the two instruments.

In reference to the vector diagram in Figure 14 the instantaneous current values are

$$i_3=I_3 \sin(wt-\theta)=I \sin(wt-\theta)$$

for balanced currents

$$i_a=\frac{V_{32}}{Z_a}\sin[wt+(150-\alpha_1)]$$

$$i_b=\frac{V_{31}}{Z_b}\sin[wt-(150+\alpha_2)]$$

where α_1 = lag angle between i_a and V_{32} and α_2 = lag angle between i_b and V_{31}. It will be noted that for stability of instrument operation it is necessary to reverse line voltage V_{13}.

For balanced inductive loads the coil average torques are

$$T_a=1/4\left[\frac{K_aIV}{Z_a}\right]\sin D[-\sin(\theta-\alpha_1)-\sqrt{3}\cos(\theta-\alpha_1)] \quad (19)$$

$$T_b=1/4\left[\frac{K_bIV}{Z_b}\right]\sin(D+\beta)[\sin(\theta-\alpha_2)-\sqrt{3}\cos(\theta-\alpha_2)] \quad (20)$$

For equilibrium $T_a=T_b$. Setting $T_a=T_b$ and solving for the deflection D, we have

$$\text{Cot } D=\frac{K_1}{K_2}\left[\frac{1-\tan\theta\cot\alpha_1-\sqrt{3}\tan\theta-\sqrt{3}\cot\alpha_1}{\sin\beta\left(\tan\theta\frac{\cos\alpha_2}{\sin\alpha_1}-\frac{\sin\alpha_2}{\sin\alpha_1}-\sqrt{3}\frac{\cos\alpha_2}{\sin\alpha_1}-\sqrt{3}\tan\theta\frac{\sin\alpha_2}{\sin\alpha_1}\right)}\right]-\cot\beta \quad (21)$$

where

$$K_1=\frac{K_a}{Z_a}, \text{ and } K_2=\frac{K_b}{Z_b}$$

If design is such that

$$K_1=K_2$$

then

$$\alpha_1=\alpha_2=\alpha$$

and equation 21 reduces to

$$\text{Cot } D=\frac{\tan\alpha(1-\sqrt{3}\tan\theta)-(\tan\theta+\sqrt{3})}{\sin\beta[(\tan\theta-\sqrt{3})-\tan\alpha(1+\sqrt{3}\tan\theta)]}-\cot\beta \quad (22)$$

In most cases α is small, and

$$\text{Cot } D=-\left[\frac{\tan\theta+\sqrt{3}}{\sin\beta(\tan\theta-\sqrt{3})}+\cot\beta\right] \quad (23)$$

When the power factor becomes zero, equation 23 is indeterminate but can be evaluated. For power factor = 0

$$\text{Cot } D=-\left[\frac{1+\cos\beta}{\sin\beta}\right] \quad (24)$$

The family of curves resulting from equation 23 are shown in Figure 15. Each curve will give one particular scale distribution. For any given scale angle one can determine from these curves the power-factor scale range for any coil angle β.

Expression 23 was derived for the condition of lagging power factor. For leading power factor the relation for the deflection becomes

$$\cot D=\frac{\tan\theta-\sqrt{3}}{\sin\beta(\tan\theta+\sqrt{3})}+\cot\beta \quad (23a)$$

This expression when plotted will give the same scale distribution as equation 23, but the angles of deflection D will have different values. However, for an instrument whose pointer is set to bisect the angle β between coils, the scale distributon will be symmetrical about the unity-power-factor point for lag and lead conditions with any given value of β.

The value of β most commonly used is the one for which $\beta=75$ degrees. This angle is selected because the resulting scale distribution more nearly approaches linearity than any other value of β.

If the curve for $\beta=75$ degrees is separately plotted as on Figure 16, the scale distribution is more clearly determined. It will be noted from this distribution that if the scale is to be lag–lead for a scale angle of 90 degrees, the maximum power-

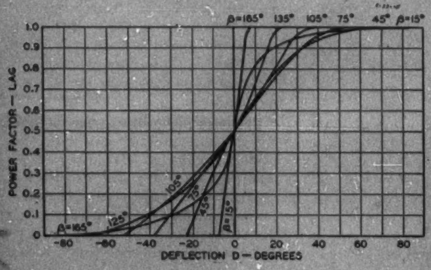

Figure 15. Scale distribution curves for a three-wire three-phase power-factor meter, plotted from the equilibrium equation

$\alpha=0$

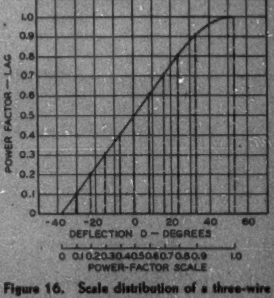

Figure 16. Scale distribution of a three-wire three-phase power-factor meter for $\beta=75$ degrees

$\alpha=0$

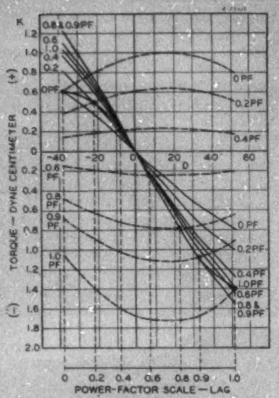

Figure 17. Scale distribution of a three-wire three-phase power-factor meter, plotted from the dynamic torque curves

$\alpha=0$ —— Coil *A*

$\beta=75$ degrees - - - Coil *B*

factor range will be approximately 0.6 lag to 0.6 lead.

Figure 17 is a plot of the dynamic torque curves which also show the resulting scale distribution. Barring inaccuracies in graphing, the distribution in Figure 17 and Figure 16 are identical.

Four-Wire Three-Phase Power-Factor Meter

The last application that shall be discussed in this article is the condition for four-wire three-phase power-factor measurement. In this case phase-to-neutral voltages are used instead of phase-to-phase voltages as in the three-wire three-phase application. Angle β is then selected to give the best scale distribution for the particular scale range desired.

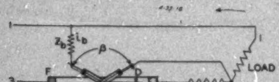

Figure 18. Diagram of a four-wire three-phase power-factor meter

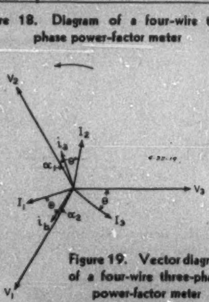

Figure 19. Vector diagram of a four-wire three-phase power-factor meter

With reference to Figure 19, which is the vector diagram of the instrument connections schematically shown in Figure 18, the instantaneous currents for a balanced inductive load using V_3 as the reference vector are

$$i_3=I_3\sin(wt-\theta)=I\sin(wt-\theta)$$

$$i_a=\frac{V_2}{Z_a}\sin(wt+120-\alpha_1)$$

$$i_b=\frac{V_1}{Z_b}\sin(wt-120-\alpha_2)$$

The average coil torques turn out to be

$$T_a=1/4\left[\frac{K_aIV}{Z_a}\right]\sin D[-\sqrt{3}\times \sin(\theta-\alpha_1)-\cos(\theta-\alpha_1)] \quad (25)$$

and

$$T_b=1/4\left[\frac{K_bIV}{Z_b}\right]\sin(D+\beta)[\sqrt{3}\times \sin(\theta-\alpha_2)-\cos(\theta-\alpha_2)] \quad (26)$$

For

$$K_1=\frac{K_a}{Z_a} \text{ and } K_2=\frac{K_b}{Z_b}$$

the expression for the deflection at equilibrium is

$$\operatorname{Cot} D=\frac{K_1}{K_2}\left[\frac{\sqrt{3}-\sqrt{3}\tan\theta\cot\alpha_1-\tan\theta-\cot\alpha_1}{\sin\beta\left(\sqrt{3}\tan\theta\frac{\cos\alpha_2}{\sin\alpha_1}-\sqrt{3}\frac{\sin\alpha_2}{\sin\alpha_1}-\frac{\cos\alpha_2}{\sin\alpha_1}-\tan\theta\frac{\sin\alpha_2}{\sin\alpha_1}\right)}\right]-\cot\beta \quad (27)$$

When the design is such that

$K_1=K_2$, and $\alpha_1=\alpha_2=\alpha$

then equation 27 reduces to

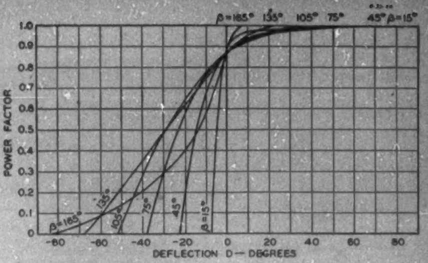

Figure 20. Scale distribution curves of a four-wire three-phase power-factor meter, plotted from the equilibrium equation

$\alpha=0$

$$\operatorname{Cot} D=\frac{\tan\alpha(\sqrt{3}-\tan\theta)-(\sqrt{3}\tan\theta+1)}{\sin\beta[(\sqrt{3}\tan\theta-1)-\tan\alpha(\sqrt{3}+\tan\theta)]}-\cot\beta \quad (28)$$

For $\alpha=0$, which is a practical assumption

$$\cot D=-\left[\frac{\sqrt{3}\tan\theta+1}{\sin\beta(\sqrt{3}\tan\theta-1)}+\cot\beta\right] \quad (29)$$

By direct comparison of equation 29 with 23 the similarity that exists will be observed. The only difference is in the location of the $\sqrt{3}$ term. This slight difference, however, makes quite a difference in the scale distribution and torque curves. For the condition when the power factor is zero the evaluated indeterminate form of equation 29 reduces to the same expression as equation 24.

The analysis of the four-wire three-phase application is quite similar to the three-wire three-phase case. For this reason details have been omitted. Expression 29 has been derived for the condition of lagging power factor, but a similar expression for leading power factor would give the same scale distribution.

From Figure 20 one can see that it is possible for a 100-degree scale to obtain a scale range of 0.9–1.0–0.9 power factor if β is set equal to approximately 75 degrees. The most common condition in practice for a lag-lead scale is for β to equal 105 degrees. The scale distribution for this condition is shown in Figure 21. Figure 22 also gives the same distribution as plotted from the dynamic torque curves.

The foregoing analyses show the theoretical possibilities of scale ranges in power-factor meters of the crossed-coil type. A variety of scale distributions is inherent and to a large extent dependent on the particular circuit application of the instrument. Limitations on ranges and distribution are necessarily imposed

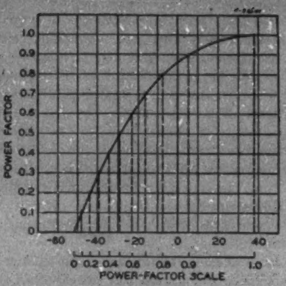

Figure 21. Scale distribution of a four-wire three-phase power-factor meter for $\beta=105$ degrees

$\alpha=0$

because of mechanical design; however, the derived distribution curves for each application should prove of value as an aid in selecting proper power-factor scale ranges for given scale angles of deflection.

Power Factor of Unbalanced Polyphase Circuits

In the previous summary of polyphase power-factor meters, emphasis was placed on the application to systems having symmetrical sets of voltages and currents or balanced loads. Although it is beyond the scope of this article to analyze mathematically the effect of system unbalance on the measurement of power factor on polyphase circuits, it might be well to consider briefly this question. It is conceivable that, for a given condition of unbalance, the torque equations of a polyphase power-factor meter could be derived, and an expression for the instrument indication obtained. This indication may or may not be an indication of the power factor of the system. The question immediately raised is: "What is the meaning of power factor in a polyphase circuit?" In the past there has been some disagreement as to what power factor is in a polyphase circuit and only recently has any definition been recorded. "The American Standard Definition of Electrical Terms" gives several definitions of power factor. To quote from this publication, they are:

> "Power factor is the ratio of active power to apparent power. The power factor Λ is given by the equation
>
> $\Lambda=P/U$
>
> [where P = active power and U = apparent power]. It follows that the power factor of a polyphase circuit must be designated as arithmetic or algebraic to indicate whether arithmetic or algebraic apparent power is used in the denominator. If both the currents and potential differences are sinusoidal and if both form symmetrical sets, then
>
> $\Lambda=\cos(\beta-\alpha)$
>
> "Vector power factor is the ratio of the active power to the vector power... If both the currents and potential differences are sinusoidal and if both form symmetrical sets, then the vector power factor is the same as the power factor."

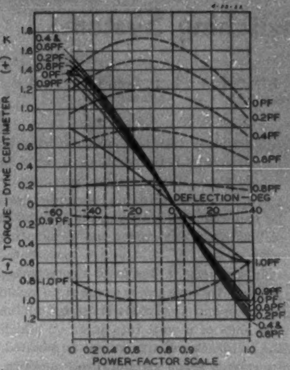

Figure 22. Scale distribution of a four-wire three-phase power-factor meter, plotted from the dynamic torque curves

$\alpha=0$
$\beta=105$ degrees
—— Coil A
– – – Coil B

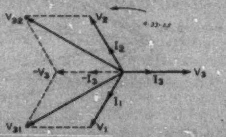

Figure 23. Vector diagram of a three-wire three-phase balanced resistance load $I_1=I_2=I_3=I$

These definitions clearly suggest that three different power factors can be realized which are determined by the use of arithmetic or algebraic apparent power or vector power. For balanced systems the three power factors are equal for all angular displacements between phase voltages and currents. In the unbalanced system the three power factors are equal only when all phase currents are inphase with their respective phase voltages and are different for other angular displacements. Unity power factor is a term that is commonly used to denote the condition when a current and a voltage are inphase.

For an example of the effect of unbalance on instrument indication let us take a simple extreme case. Suppose that the three-wire three-phase instrument shown in Figure 13 is connected to a balanced pure resistance load. As unity power factor, previously defined, exists in this system, let us further assume that the instrument is calibrated to indicate this unity power factor. Now let us unbalance the load by opening the connection to the load at two, thereby making the current in this phase equal to zero. Also let the three line voltages remain a symmetrical set and the instrument potential circuits be excited. For this extreme condition of unbalance the current vector I, which originally was inphase with phase voltage V_3, will now have shifted 30 degrees leading V_3 since it will be inphase with line voltage V_{31}. Although unity power factor still exists for the unbalanced condition, the instrument no longer indicates unity power factor but is in error by 30 electrical degrees. Vector diagrams shown in Figures 23 and 24 will serve to clarify these two conditions.

One can see that it is possible to have several combinations of system unbalance that might conceivably produce the same instrument indication. If this is true, then any given instrument indication would tell the observer nothing concerning the condition of circuit unbalance that produced that indication. Conversely, as previously pointed out, for a given unbalance an instrument indication might be obtained by means of a mathematical derivation that would tie up the indication with a particular unbalance condition, but what would be the practical value of this since the condition of system unbalance is already known?

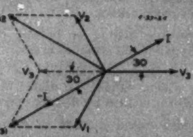

Figure 24. Vector diagram of a three-wire three-phase resistance load–balanced voltages $I_2=0$, $I_1=I_3=I$

For the standard adopted application of power-factor indicators on polyphase circuits it is reasonable to assert that, unless the polyphase system is balanced, the power-factor-meter indication is without significance.

Reference

1. ELECTRICAL MEASUREMENTS (book, second edition). F. A. Laws. McGraw-Hill Book Company, Inc., New York, N. Y., 1938.

Crossbar Toll Switching System

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IN AUGUST of 1943 a new mechanical toll switching system was placed in service in Philadelphia. It was designed by engineers of the Bell Telephone Laboratories and of the department of operation and engineering of the American Telephone and Telegraph Company for large toll centers, particularly those where most of the offices are of the panel and crossbar types. The Philadelphia installation was planned before the war. Even then it was apparent that a mechanical switching system was needed urgently to secure faster and more economical switching. War conditions produced an abnormal growth in toll traffic that accentuated the need for the new equipment. The system was therefore installed as rapidly as the heavy load of war work on the engineering and manufacturing organizations of the Bell System would permit. It is now contributing materially to the war effort by providing adequate switching facilities for the increased volume of traffic.

This new toll switching system includes the following important features:

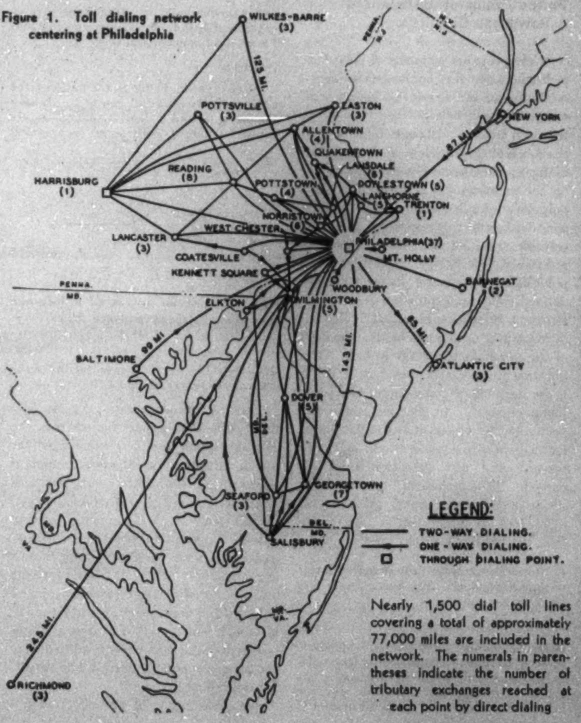
1. Crossbar switching equipment with senders and markers.
2. Operator dialing or pulsing over toll lines to panel and crossbar areas.
3. A cordless switchboard for handling traffic which cannot be dialed or pulsed through the system by distant operators.
4. A new rapid method of transmitting switching codes known as a-c key pulsing.
5. Four-wire switching, that is, the provision of separate conductors for voice transmission in each direction.

The function of toll switching equipment is to provide connections that will enable subscribers to converse with other subscribers in localities too distant to be reached over their own local network. If the toll areas in which the calling and called subscribers are located have a sufficient community of interest, there may be direct circuits between them, and at each terminal there will be a switched connection to a local office. Where the call involves two toll offices having no direct circuits between them, it must be routed through one or more intermediate toll offices, and switching connections will be required at each intermediate point as well as at the originating and terminating offices.

In the past it has been the ordinary practice to have every connection made by an operator who inserts plugs on the two ends of a cord into jacks on which the toll lines and trunks are terminated. In recent years an improved practice, known as toll-line dialing, has been applied in a few of the smaller toll areas where step-by-step switching equipment is used in the local offices. With this method, the outward toll operator who accepts the subscriber's call plugs into a toll line, and then by means of a dial temporarily associated with her cord sends dial pulses to direct the movement of switches in the distant toll office to select a trunk to the called subscriber's local office. She continues by dialing the subscriber's listed number to drive switches in the local office to establish a connection. The subscriber's bell is then rung automatically by the local circuit. If intermediate switching is involved, the operator also dials the connection through the intermediate office. Thus the manual labor of establishing connections at intermediate and inward toll switchboards is eliminated, the outward operator's work-time is reduced since she does not have to pass orders for connections and wait for them to be established, and the subscriber receives faster service.

In the toll dialing operation just described, the switching equipment in toll and local offices is all of the step-by-step variety, the movement of switches being under control of impulses dialed on a decimal basis. In areas where the local

Figure 1. Toll dialing network centering at Philadelphia



Nearly 1,500 dial toll lines covering a total of approximately 77,000 miles are included in the network. The numerals in parentheses indicate the number of tributary exchanges reached at each point by direct dialing

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switching equipment is of the panel and crossbar types, on the other hand, the action of switches is controlled by senders, with the aid of decoders or markers. The new toll switching equipment permits the introduction of toll-line-dialing operation in such areas as well. The term "toll-line dialing" is of generic character and includes not only the use of actual dials by operators but also the use of keysets and senders that generate signals of a different nature. The extent to which toll-line dialing is used in the Philadelphia area is shown in Figure 1.

There are, of course, many toll-line groups in the Philadelphia toll office not shown in Figure 1. These are groups that at present cannot economically be arranged for toll-line dialing. Cordless toll positions are provided as part of the new toll switching system to provide for handling calls over these other groups until such time as they can be arranged for toll-line dialing. They are equipped with keysets to direct the establishment of connections for the manual groups of lines just as connections for the dial groups are controlled by dials or keysets at distant positions.

Basic Arrangement

The crossbar switch used in the new toll switching system is shown in Figure 2. Since it has been described in detail by Scudder and Reynolds,[1] it will be sufficient to describe it as a co-ordinate arrangement of contacts giving ten lines on one ordinate access to ten or 20 on the other.

All toll lines and trunks are terminated on crossbar switches, and their interconnection is accomplished by senders, controllers, and markers with their connecting equipment. These units are common either to the entire office or to a substantial portion of it. The circuits comprising the common equipment are relatively complex and expensive, and are held only for the brief periods required for establishing connections. The information needed by the common equipment to guide it in setting up a connection is supplied in the form of a numerical code, usually of three digits, by an operator either in the same office with the equipment or in a distant office. The code is received by an incoming sender, which forwards it to a marker. The marker, by means of temporary connecting equipment, attaches itself to the switches on which the two circuits to be connected are located and establishes a connection between them. The relation of the common equipment to the toll lines, trunks, and switches is indicated by the schematic diagram in Figure 3.

Figure 2. A 10 by 20 crossbar switch

Operating Methods

The manner in which calls through the toll crossbar system are handled from the operator's viewpoint may be demonstrated by use of a few examples.

OUTWARD CALLS

Assume a subscriber in Philadelphia is calling a subscriber in Harrisburg whose number is 6-5432. The Philadelphia end of the connection is illustrated diagrammatically by Figure 4.

The outward operator at a switchboard of the familiar cord type will select an idle trunk to the toll crossbar office and will then dial or key the code 321, which represents Harrisburg, followed by the called subscriber's number, 6-5432. The crossbar equipment will use the three code digits, 321, to select an idle toll line and will then transmit the remaining digits over the toll line to drive switches to the called subscriber's line. The subscriber's bell will be rung automatically, and, when the subscriber answers, the lighted cord lamp in front of the outward operator will be extinguished. On station-to-station calls, the operator then starts timing the conversation for charge purposes. At the end of conversation both cord lamps will light to indicate the hang-up of calling and called subscribers, and the operator will pull down her cords. This will release the entire connection.

Now assume an outward call to a Chicago subscriber. The Philadelphia–Chicago group operates on a ring-down basis, and the outward operator, after selecting the tandem trunk, will dial or key only the three-digit code representing the Chicago group. The crossbar equipment will make the connection to an idle Chicago toll line and cause ringing current to be sent over the line. This will light a lamp associated with the toll line in Chicago, where an operator will answer the call. The outward operator in Philadelphia, will pass the order to the Chicago operator, who will complete the call. The outward operator must remain on the connection until the called subscriber answers, in order to start timing, because supervisory signals to indicate the called subscriber's answer and hang-up are not returned over a ringdown line. At the end of conversation she will pull down her cords when the calling subscriber hangs up. This will release the connection through the switches to the Chicago toll line. The inward operator at Chicago

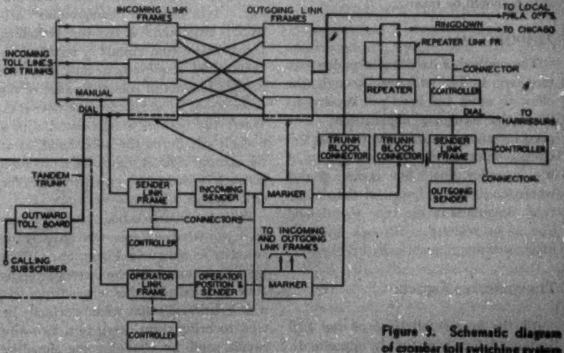


Figure 3. Schematic diagram of crossbar toll switching system

will release her connection upon receipt of a lighted cord lamp indicating that the called subscriber has hung up.

Inward Calls

An inward call over a dial toll line, illustrated by Figure 5, requires no operator in the crossbar toll area. Assume the call is to the Philadelphia number Market 1234. The distant operator will key or dial MAR-1234. The crossbar equipment will use the MAR (627) to select an idle trunk to the Market local office and the remaining digits to drive switches in that office to the called subscriber's line. The connection will be released at the end of conversation when the distant operator releases her connection.

An inward call over a ring-down toll line is represented by Figure 6. In response to ringing current on the toll line, a cordless position will be connected automatically to the line. An operator at the position will receive the order for Market 1234 and will key the complete number—MAR-1234—on her keyset, causing the connection to be established as described. Her position is then released from the connection. Since the ring-down line can give no indication at the inward end that it has been released at the outward end, the toll crossbar equipment must recognize the called subscriber's hang-up in order to release the connection. This is done by equipment in the relay circuit associated with the incoming toll line, which times for two seconds after he hangs up, and, if he has not again removed his receiver from the switchhook, releases the connection. If the called subscriber flashes, if his line is busy, if he fails to answer, or if the outward operator rings on the toll line, an inward operator (not necessarily the one who originally handled the call) is again connected to the line, and lamps on her position indicate what action should be taken.

Through Calls

The method of handling incoming calls that are to be switched through the office to other toll lines will be obvious from the foregoing explanation. The need for an operator in the crossbar toll office is determined only by the nature of the incoming line. She is not needed when the incoming line is of the dialing type, regardless of the type of outgoing line to be selected. Through traffic is illustrated diagrammatically by Figure 7.

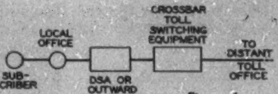


Figure 4

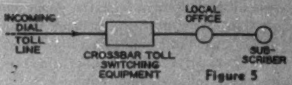


Figure 5

Transmission Aspects

All the longer toll lines of the Bell System are four-wire, that is, opposite directions of transmission are over separate paths, each with one-way amplifiers at suitable intervals. These two paths are combined by means of a hybrid coil into a single two-wire circuit before reaching the telephone at each end.

At switching offices in this country, all four-wire circuits have been previously reduced to two-wire by a hybrid coil before they reach the switching point and thus have been switched as two-wire circuits. This is indicated in Figure 8a which shows a four-wire toll line at the left switched to a two-wire toll line with a terminal repeater. There are limits to the transmission efficiency of such arrangements, since the two-wire junction permits speech currents traveling in one direction to be reflected partially toward the talker at the hybrid coil, thus causing echoes. When there are two-wire junctions at both ends of the circuits, there is a closed path around which "singing" may result. In addition, when cord circuit repeaters are added at a two-wire switching point there is a two-wire junction on each side of the repeater, and more echoes result—being made greater by the gain of the repeater. A similar result is obtained at a "switching-pad" office when gain is effectively inserted by removing two-wire switching pads instead of by adding a repeater. Since any circuit in the office must be capable of connection to any one of a large number of other circuits, which may not all have the same characteristics, the balancing network at the junction obviously cannot be made to balance accurately all of them individually. As a result, a "compromise" network is employed. A two-wire switching point, where cord circuit or terminal repeaters are employed, is thus always a potential source of echoes because of the necessity of using compromise networks. Where the network is associated with a a single circuit, on the other hand, as at the extreme right of Figure 8a, a precision network is used which greatly reduces the echo volume.

With the new crossbar system all lines are terminated on a four-wire basis at the switches as shown in Figure 8b. When these lines are four-wire circuits, this is done by omitting the terminating sets with hybrid coils that would normally be used to bring them down to a two-wire circuit, and by changing the signaling means, switching pads, operators' connections, and other circuit elements to a four-wire basis. On the two-wire toll lines with terminal repeaters the same general result is obtained by removing the hybrid coil on the office side of the repeaters. On two-wire toll lines without terminal repeaters, a hybrid coil is added with one two-wire branch toward the two-wire line and the other toward the balancing network, and with the four-wire branches connecting to the switches. The net result is the same as the two-wire repeatered toll line in Figure 8b with the two amplifiers and switching pads removed.

When two four-wire or two-wire toll lines with terminal-repeaters are switched together on a four-wire basis, all sources of echo in the switching office are eliminated. Some echo is of course returned from the hybrid coil connecting the two-wire repeater to the two-wire line, but this can be kept small by keeping the balancing between the precision network and its associated line high.

If the two-wire toll line of Figure 8b did not have terminal repeaters or switching pads, the switching pads in the four-wire toll line would be removed, thus effectively inserting gain in the over-all connection. Because of the high balance at the two-wire hybrid coil, considerable gain may be inserted in this manner, thus allowing greater loss in the nonrepeatered two-wire circuit. This greater loss permits saving in copper and loading and frequently permits omitting a terminal two-wire repeater that would be required with a two-wire switching system.

With four-wire switching, therefore, certain of the two-wire lines, chiefly those that are not used as intermediate links in long built-up circuits, may be made of the high-loss type. If two of these high-loss lines are to be connected together directly, there may be too much over-all loss from subscriber to subscriber. To avoid this, repeaters are switched in, thus reducing the over-all loss. There are no objectionable effects because of echoes or singing because at the point where these repeaters are inserted the circuit is four-wire, and thus there are no hybrid coils to act as a source of additional echoes. The hybrid coils, where the four-wire circuit is connected to the outgoing two-wire line, are precision networks, and even with the additional gain the echoes do not become objectionable, the two-wire balances with precision networks inserting loss in the echo returned toward the subscriber. The insertion of repeaters is done automatically depending upon markings associated with the two lines connected.

These high-loss two-wire lines may be lines to other toll offices or connecting trunks to local offices. The determination of which trunk should be high loss and which low loss depends upon the economy which may be obtained in outside plant and terminal repeaters compared to the cost of providing additional gain when it is required.

Switching pads are provided on four-wire toll lines and on two-wire toll lines with terminal repeaters to permit the loss to be adjusted for the different types of connections that may be involved. With two-wire switching, special hybrid coil arrangements and means for inserting or removing switching pads were necessary to permit satisfactory talking in both directions on four-wire circuits.

Potentially, four-wire switching offers considerable promise. The percentage of four-wire toll lines will increase in the future both because long circuits tend to increase at a greater rate than do shorter circuits and because the general use of carrier throughout the toll plant will tend to reduce the minimum lengths of four-wire toll lines which are economical. With four-wire circuits and four-wire switching in general use, many improvements in echo suppression and volume control would become possible and economical.

Progress of Calls Through the System

The manner in which a connection is established through the crossbar equipment may be demonstrated by pointing out the function of each item of equipment in Figure 3 as the call progresses. For this purpose it is not necessary to know the composition of the individual parts, which will be described later.

The crossbar switches on which the incoming trunks and toll lines are terminated are assembled in groups called incoming link frames, and those on which the outgoing trunks and toll lines are terminated are called outgoing link frames. These two types of link frames are interconnected so that any incoming trunk or toll line may be connected to any outgoing trunk or toll line.

DIAL OR KEY-PULSING CALL

The example previously used of the subscriber in Philadelphia who has originated a toll call to Harrisburg 6-5432 may again be employed. The outward operator selects a "tandem" trunk connecting to the crossbar toll switching equipment. Relays associated with this tandem trunk in the crossbar system signal the sender link frame that an incoming sender is required. A controller circuit then becomes attached to the sender link frame and connects the tandem trunk through the frame to an incoming sender. The operator then keys or dials the code 321, which is assigned to the Harrisburg toll-line group. She also keys the called number 6-5432, and these digits as well as the code digits are recorded by the sender. The sender in turn obtains access to a marker and transmits the Harrisburg code to it. This code is registered and translated by the marker which causes a route relay in the marker to operate. This relay directs the marker to the proper trunk-block connector where the test leads for all the toll lines to Harrisburg are located. Upon gaining access to the connector, the marker selects an idle toll line and immediately makes that line appear busy to other calls.

To connect the tandem trunk to the selected Harrisburg toll line, the marker must identify the incoming and outgoing link frames on which these are located. The signal identifying the outgoing link frame is furnished to the marker over a lead from the outgoing toll line through the trunk-block connector, while the incoming link frame is identified by a signal which the marker receives from the tandem trunk over one of the leads through the sender link frame, and the sender.

The marker then gains access to the incoming and outgoing link frames involved and excludes all other markers from these frames. It then selects an idle path through these frames and operates the proper magnets of the crossbar switches to establish the connection. After this the marker releases and disconnects itself from the incoming and outgoing link frames as well as from the sender and the trunk-block connector. The total time required for the marker to perform all its functions is about one second.

When the toll line to Harrisburg has been selected, an outgoing sender will become associated with it through a sender link frame under the direction of a controller. The incoming sender will then transfer the called number to the outgoing sender, and that sender will control the switching equipment at Harrisburg through which the call is completed. Upon completion of their functions, the incoming and outgoing senders are dismissed.

RING-DOWN CALL

When, as in the case of the call to Chicago, the outgoing toll line is arranged for ring-down operation, the outgoing sender is not used. The operator will key only the code number of the toll-line group. This is sufficient to cause the call to be routed to the Chicago toll line as described for the previous call. A ringing signal is then transmitted by relay equipment associated with the toll line to signal the Chicago operator.

CALLS REQUIRING OPERATOR ASSISTANCE

Incoming calls from ring-down toll lines require operator assistance at the crossbar toll office as previously mentioned. An incoming signal from such a toll line causes the line to be connected through an operator link frame to an operator position. This position is of the cordless type and is equipped only with keys and lamps. The operator is automatically connected to the toll line and receives the necessary details of the incoming call from the outward operator. She then operates keys which cause the called number to be registered in a sender associated with the position. This sender is then associated with a marker, and the call is completed in the manner previously described.

In general, the operator at the crossbar toll office is released from the call as soon as the positional sender has completed its functions. If the call is not completed, as when the called subscriber's line is busy, an operator is reconnected to the call, and the status of the call is indicated by lamp signals at the position. The operator then makes the proper oral report to the calling operator. If the crossbar toll operator is required to take further immediate action on a call, the operator may remain associated with the

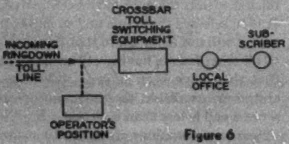

CROSSBAR TOLL SWITCHING EQUIPMENT
INCOMING RINGDOWN TOLL LINE
SUB-SCRIBER
LOCAL OFFICE
OPERATOR'S POSITION

Figure 6

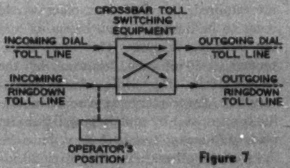

CROSSBAR TOLL SWITCHING EQUIPMENT
INCOMING DIAL TOLL LINE
OUTGOING DIAL TOLL LINE
INCOMING RINGDOWN TOLL LINE
OUTGOING RINGDOWN TOLL LINE
OPERATOR'S POSITION

Figure 7

call even after the called number has been registered in the positional sender. This is accomplished automatically under conditions always requiring this association, or the operator may operate a key to retain association with the call.

THROUGH CALL REQUIRING A SWITCHED-IN REPEATER

If an incoming call from either a dial or ring-down toll line is switched to another toll line of either variety and both lines are high loss, a switched-in repeater is required. The marker is told by a d-c potential on a lead of the incoming line that it is high loss and by a contact of the route relay associated with the outgoing toll-line group that the outgoing line is high loss. Under these conditions, as soon as the marker has established the connection, it will send a signal to the outgoing line which will cause it to call for a repeater. A controller will thereupon be attached to the repeater link frame on which the outgoing line appears and will insert a repeater in the connection. The marker and controller are released as soon as their functions are completed, and the repeater remains connected for the duration of the call.

Principal Elements of the System

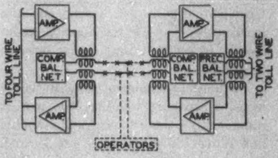
1. INCOMING AND OUTGOING LINK FRAMES

A "frame" is an assembly of switches interconnected as an electrical unit to serve a group of either incoming or outgoing lines. The incoming and outgoing link frames are composed of a primary and a secondary bay of switches, each ten switches high, with as many supplementary bays as are required to accommodate the number of trunks necessary to make efficient use of the call-carrying capacity of the frame. Incoming trunks appear on the primary and supplementary primary bays of the incoming frames, and outgoing trunks appear on secondary and supplementary secondary bays of outgoing frames. Each bay has a capacity of 100 trunks. The links connecting the primary and secondary bays are called *A* links, those connecting the secondary bays of the incoming frames and the primary bays of the outgoing frames are called *B* links or junctors, and those connecting the primary and secondary bays of the outgoing frames are called *C* links. Each incoming frame has connections to all outgoing frames but none to other incoming frames, and the reverse is true of outgoing frames. A simplified schematic diagram of the arrangement is indicated in Figure 9.

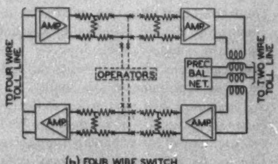
It will be noticed that *A* and *C* links interconnect switches on a frame, while *B* links interconnect frames. The pattern of *A* and *C* links is fixed, and there are always 200 on a frame in ten groups of 20 each, so that each trunk has access to 20 *A* or *C* links. While the distribution of *B* links follows an orderly pattern, it will vary with the number of frames, which is, of course, dependent upon the size of the office.

There are always at least 20 *B* links from each incoming frame to each outgoing frame and therefore 20 paths by which an incoming trunk can reach a particular outgoing trunk. Each path consists of three links in series, and between any one incoming and any one outgoing line, a particular *A* link can be used with only one particular *B* and *C* link, and thus, if one is busy, the entire path is unavailable for use on the call. The method of selection employed by the markers is such that the lowest numbered path, all elements of which are idle, is used. This is the simplest method and has the additional advantage of giving a lower probability of blocking a call because of inability to match links than would a random choice of links.

Since there are only 200 *B* links per frame and 20 links to each opposing frame are required, the size of an office would seem to be limited to ten incoming and ten outgoing frames. This limit is extended to 20 of each, however, by the simple device of providing a supplementary bay of secondary switches on incoming frames, and by multiplying *B* links on pairs of frames so that each may share the links of the other. An office with 20 incoming frames and 20 outgoing frames is comparable in size to a 10,000-line local crossbar office.



(a) TWO WIRE SWITCH



(b) FOUR WIRE SWITCH

Figure 8. Typical transmission paths through switching offices

In the Philadelphia installation it was found desirable to provide two separate trains of switches, one for outward and through traffic, the other for inward traffic. The system is described in this paper, however, as a single train system, since all that is required for a clear understanding of its operation may be presented on that basis.

2. OPERATOR LINK FRAMES

As shown in Figure 3, an operator link frame is used to connect incoming manual trunks to operators' positions. It is composed of 100-point (ten by ten) crossbar switches in primary-secondary alignment and accommodates 100 trunks on the primary side and 40 cordless positions on the secondary side. Each group of ten trunks is served by four links, each of which gives access to ten positions. Figure 10 depicts half an operator link frame, the other half being an exact duplicate of it with the trunks multiplied to both halves.

The establishment of connections through operator link frames is directed by a group of controllers in much the same manner as the markers direct connections through incoming and outgoing frames. The controllers gain access to trunks, links, and positions through controller connectors consisting of multicontact relays.

To obtain substantially uniform treatment of calls and particularly to guard against "orphan calls," the "gate" principle of serving them is employed. When a controller is attached to a link frame, it scans the ten subgroups of trunks and admits within the gate all which have at least one waiting call. Those which have no waiting call will be locked outside, and calls which may subsequently arrive on them will not be served until the frame has been released and has secured a new controller. The controller then selects one of the admitted subgroups and locks out the individual trunks of that subgroup which do not have waiting calls. Having served all accepted waiting calls in that subgroup, it proceeds to the next subgroup, treats it in the same manner, and so continues until all subgroups admitted within the gate have been served. It then releases the frame and so marks it that it will not again serve that frame until all other frames having calls waiting at that time have been served. When operators are available, the time required to serve a call is less than a second.

Several precautions are taken to insure

even distribution of the load to operators:

(*a*). A standard pattern for cabling positions to link frames has been carefully constructed with that end in view.

(*b*). Half of the controllers test for idle positions in one direction, the other half in the opposite direction.

(*c*). The position appearances on the frames are divided into small groups, and a controller starts testing with a different group on each call until a complete cycle has been made.

3. Sender Link Frames

Sender link frames are similar to operator link frames and are used to give access to both incoming and outgoing senders. Two-way dial toll lines appear only on incoming sender link frames and obtain access to both types of senders through them, a portion of the outgoing senders being multipled to some incoming sender link frames for that purpose. The connections through sender link frames are directed by controllers in the manner described for operator link frames.

4. Repeater Link Frames

Repeater link frames are similar in construction and operation to operator and sender link frames but provide 60 links giving access to 60 repeaters to serve 100 outgoing toll lines. The greater link capacity is required because the repeaters are held for the duration of the conversation.

5. Cordless Switchboard Positions

The switchboard positions are of the cordless type, all functions performed by the operator being effected by key operations. The physical appearance of the switchboard is shown in Figure 11.

Calls reach a position through three channels known as loops, and it is the loops which appear on the operator link frames. Triplicate channels are provided to permit calls which require continuing attention by the operator to be held on the position and to permit overlap operation, that is, the admission of a new call to a position before complete disposition has been made of a previous call. Each loop appears on the position as a key and three associated lamps. Normally calls are connected to the operator's telephone circuit automatically, but, when a loop is held to her position, she may connect her telephone circuit to it at will, and the key is provided for this purpose. The lamps are:

(*a*). A guard lamp which flashes when the loop is connected to the operator's telephone circuit.

(*b*). A busy lamp which is lighted steadily as long as the loop is held to a trunk through the link frame.

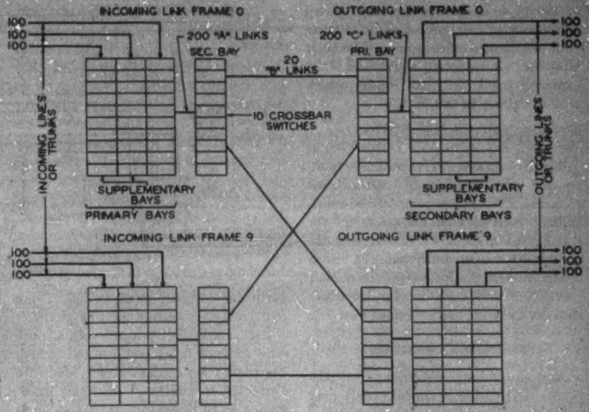


Figure 9. Links between incoming and outgoing trunks

(*c*). A supervisory lamp which follows signals received from the called trunk.

A complete sender is associated with each position and appears on the marker connector frame for connection with the marker. To permit overlap operation, however, the sender is actually divided into two parts, that part concerned with the code digits being associated with the operator's telephone circuit and the remaining part functioning independently as a normal incoming sender. The two parts may be simultaneously connected to two different trunks through two of the position loops.

The keyset used by the operator in pulsing consists of 11 strips of ten-button keys and a start key. The setting of the first three strips is transferred to the marker for selection of the called trunk, and that of the remaining strips is transmitted to an outgoing sender, or to a sender in a distant office if the outgoing trunk is to an office equipped to receive a-c key pulses.

A number of lamps and keys common to the position are provided to apprise the operator of the proper course of action to be followed and to enable her to take the necessary action to meet the various conditions which are presented to her.

6. Senders

The function of a sender is to accept incoming pulses, interpret them, register them as numerical digits, and transmit them forward for the purpose of controlling switching operations. The wide variety of trunks which must be interconnected by this system has made it advisable to provide separate incoming and outgoing senders, and to transfer to the outgoing senders those digits which must be used to control switching operations in distant offices. Although this method of operation results in several types of senders in the office, each sender is less complicated, since it may be arranged for only the kind of pulsing required by the trunks with which it is associated. Outgoing trunks arranged for a-c key pulsing do not require an outgoing sender, the pulses being sent from the incoming sender directly to a receiving sender in the distant office.

There are two groups of incoming senders, one arranged to receive dial pulses, the other to receive a-c or d-c key pulses. Both are arranged to transmit either d-c pulses to an outgoing sender in the same office or a-c key pulses to a sender in a distant office. D-c key pulsing represents digits by combinations of high and low conditions of two polarities of battery over two wires and ground and is limited to use between items of equipment in the same building. A-c key pulsing employs combinations of frequencies in the voice range, and may be used for any distance. The key-pulsing senders are signaled by a d-c potential on a lead of the incoming trunk whether to prepare for reception of a-c or d-c pulses.

There are also two groups of outgoing senders, one arranged to transmit either the type of pulses required by the equipment in local panel offices or the type used to reach manual offices in a panel area, the other to transmit either dial pulses

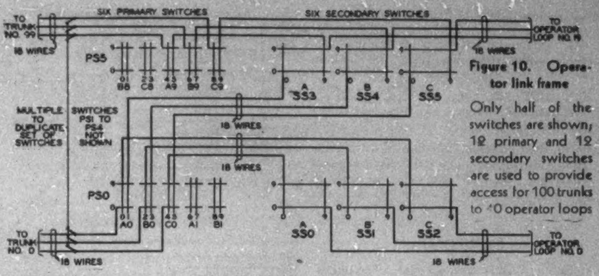


Figure 10. Operator link frame

Only half of the switches are shown; 12 primary and 12 secondary switches are used to provide access for 100 trunks to 20 operator loops

of the type used to drive step-by-step equipment or call announcer pronouncements to manual offices equipped for their reception. A d-c potential on a lead of the outgoing trunk tells the sender which kind of pulsing to transmit.

7. MARKER CONNECTORS

Marker connectors give incoming senders and cordless position senders access to markers. They are composed of multicontact relays. One marker connector frame gives 18 senders access to all markers through two separate channels. Each sender, therefore, appears twice on marker connectors and each marker as many times as there are marker connectors.

8. TRUNK-BLOCK CONNECTORS

A group of outgoing trunks to a particular destination is spread evenly over outgoing frames. To enable the marker to test the desired group and select an idle trunk, however, it is necessary to assemble test leads representing the group at some point. The sleeve and select magnet leads of all trunks are brought to the trunk-block connector for this purpose.

A single connector accommodates 400 trunks with duplicate appearances and gives all markers access to them. A block consisting of 40 trunks may be a complete trunk group so far as destination is concerned, only part of a group, or several groups.

The select magnet lead which is brought through the trunk-block connector carries information to the marker identifying the location of the trunk on the outgoing frame. Since it is frequently necessary to make temporary reassignments of toll lines, the trunk-block connector is supplemented by assignment patching jacks through which the sleeve and select magnet leads of many toll lines are carried.

9. MARKERS

The several hundred relays comprising the marker, which is the "brains" of the system, form a number of related but somewhat independent groups with different duties to perform. Only the more important of these will be discussed.

One group of relays checks the integrity of the leads between the incoming sender and the marker. A second group registers the three digits comprising the code for the outgoing trunk group. A third group, part of which are of the multicontact class, serves to convert the three-digit registration into the marking of a single terminal which may be cross-connected to a route relay representing a particular outgoing trunk group. These route relays constitute another group.

Each marker is equipped with a route relay for each trunk group to be reached. The route relay carries contacts which are connected to punchings providing information as to the nature of its associated trunk group and the means of locating it. Some punchings guide the marker to the selection of the trunk block on which the group appears and tell it where to begin and end testing of the trunks in the block. Others tell it whether the trunks are of a variety requiring the insertion of voice repeaters on some connections and supply information to control the action of incoming senders and cordless positions.

In another group 40 relays are simultaneously connected to the sleeve leads of the trunks through the trunk block in order to select an idle trunk in the desired group. Another group of relays tests *A*, *B*, and *C* links, matching them for an idle path through the switches of the incoming and outgoing frames. A chain of 20 pairs of multiwound relays is used for this purpose. In setting up a connection, an *A*, a *B*, and a *C* link in series form one channel, and a set of this sort is connected to three of the windings of each pair of test relays. If any winding is energized, the associated channel is unavailable for use on the connection. The first pair of relays in the chain remaining unoperated selects the channel to be used.

A set of vacuum tubes and filters is used for frame identification, that is, for determining on which frames the two trunks to be interconnected are located. This information is presented to the marker in the form of three frequencies imposed on the select magnet leads of the trunks. The frequencies used are the odd harmonics of 85 cycles from 425 cycles to 1,615 cycles.

A-C Key Pulsing

One of the interesting new features of this switching system is the use of a-c key pulsing to transmit digits over toll lines. Exchange names and subscribers numbers have previously been transmitted by d-c key pulsing within an office, but this is not practicable for transmission over long toll circuits. A-c key-pulsing signals, however, can go wherever the speech currents can go and at the same speed, so there is no limit on the range of this method.

When an operator uses a suitable key-set or when the sender recognizes by suitable marking that the circuit over which it is to send a number is equipped for a-c key pulsing, a *KP* signal is sent out which consists of 1,100 cycles and 1,700 cycles, simultaneously. At the distant end, the reception of the two frequencies makes the a-c key pulsing receiver sensitive to further signals. The numbers or letters are then sent one at a time, each represented by two of the frequencies 700, 900, 1,100, 1,300, and 1,500 cycles. Finally, a signal consisting of 1,500 and 1,700 cycles simultaneously is sent to indicate the end of the number and to make the receiver insensitive again. The receiving sender then proceeds to set up the call.

The receiver is insensitive to other currents such as noise or speech, except during the interval while the signals are being sent. To avoid a false *KP* signal and to protect against wrong numbers during the sensitive interval, the receiver is arranged so that two and only two frequencies can indicate a number. If the proper signals are not received, supervisory signals are sent back to notify the operator originating the call by a flashing light that she must put the call through again.

By a-c key pulsing, it is possible to transmit numbers over any satisfactory telephone circuit faster than any operator can send them. An incoming dial sender of the new system, for example, receives numbers from an operator at the calling end of the trunk at the rate of about one digit per second, while it will send numbers over the outgoing trunk at the rate of ten digits per second. The use of pulsing also gives a one-way check of the transmission continuity of the toll line.

Facilities for Handling Delayed Traffic

The cost of long-distance circuits has to date rendered it imperative to engineer them on a delay basis in order to maintain toll service at a reasonable cost to the subscriber. This means that in the busy hours, calls will frequently encounter "no-circuit" conditions. Under such conditions it becomes desirable to relieve operators of the burden of making repeated unsuccessful attempts to secure a circuit. This purpose is accomplished by means of overflow circuits.

An overflow-control circuit is associated with each toll-line group appearing on outgoing frames and permits a maximum of from one to four simultaneous connections to it depending on the size of its associated toll-line group. The trunk circuits affording entrance to the overflow-control circuit appear on the trunk-block relay along with the associated toll lines. A call for a group having all circuits busy is connected to over flow. As long as all circuits are busy, the overflow circuit returns a slow flash to all incoming circuits connected to it, and, when a circuit becomes available, a fast reorder flash is returned. If all circuits of a group are busy and all of its associated overflow trunks are also busy, the call will be routed to one of a group of master busy trunks common to the office. Master busy trunks return a distinctive irregular flash.

The flashes are returned to the originating operator on calls employing dial toll lines or tandem trunks. On calls coming in over ringdown toll lines, the cordless operator is held if the connection is routed to overflow. She is given a lighted overflow pilot lamp, and the loop is automatically locked to her position so that she may subsequently take action when a circuit becomes available.

Occasionally in normal times, very heavy overloads of traffic on some groups occur because of holiday demands or unpredictable catastrophes. Under war conditions these overloads occur rather frequently. When they occur, calls tend to accumulate in quantities which will tax the capacity of an affected trunk group for extended intervals.

Under these conditions, facilities are required to enable the operating force to serve calls as nearly as practicable in order of their filing time. For this purpose, a manual call-order board is provided. This board is similar to other cord switchboards except that twin plugs and jacks are used so that through calls may be established on a four-wire basis. The call-order operator establishes a through connection by first plugging one end of a cord into a tandem trunk and keying over it to secure the toll line, then repeating the operation with the other end of the cord to secure the other toll line. Her position is equipped with a ten-button keyset. Whenever circuit groups become so congested that calls cannot be served within five or ten minutes, call orders for through calls are assembled at the call-order board.

When congestion develops to the point where delay must be quoted, a patch is made from the overflow-control circuit to a delay-quoting trunk appearing in the call-order switchboard. As long as connections are in overflow, a call-order operator will repeatedly quote the delay indicated by the trunk. When the patch has been made, all operators except those at the call-order board are denied access to the group. The outward operators in the same toll-center area must then leave requests with the call-order operators to secure circuits for them.

Maintenance Facilities

Perhaps the most interesting of the maintenance facilities is the marker trouble indicator. When a marker encounters trouble in the process of setting up a connection, it calls in the trouble indicator and leaves a record of the trouble. The record is displayed on lamps which indicate how far the marker had progressed with the call, what individual equipment items had been involved, and, where possible, the nature of the trouble encountered.

A similar trouble indicator on a smaller scale is provided for controllers. Automatic routine testing equipment for senders and outgoing trunks is also provided.

The new toll testboard associated with the system carries twin-jack appearances of toll lines to permit four-wire switching. It is also equipped with dialing and key-pulsing apparatus to enable it to operate with the mechanical equipment.

Traffic Meters

A comprehensive set of measuring apparatus is provided to keep the supervisory force informed as to the performance of the office. This equipment, consisting of ampere-hour meters, ammeters, recording ammeters, message registers, and lamps records traffic load carried by various elements of switching equipment, delayed calls, and other items of interest to the office management and to the traffic engineer.

Conclusion

The new switching system offers many improvements over the switching equipment which has been in use in the large toll centers in the past.

Transmission objectives are met with greater ease, and substantial economies are made in the outside plant and in repeater equipment.

It provides facilities which enable outward toll operators to direct the establishment of connections without the aid of inward or through operators. This results in operating economies and improved service to the subscriber. The provision of a cordless switchboard to handle traffic which must remain for some time on a ring-down basis enables that traffic also to be handled more expeditiously and with operating economies.

The use of senders and markers for controlling the establishment of connections will undoubtedly play an increasingly important part in future long-distance switching, since these devices permit more complete mechanization of the long distance operating job. The switching system which has been described is, therefore, inherently well adapted to meet the future requirements of the continually improving long-distance-switching network now functioning in the United States.

Figure 11. Cordless switchboard

Reference

1. CROSSBAR DIAL TELEPHONE SWITCHING SYSTEM, F. J. Scudder, J. N. Reynolds. AIEE TRANSACTIONS, volume 58, 1939, May section, pages 179–92.

Regulation of A-C Generators With Suddenly Applied Loads

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DETERMINATION of the voltage drop at the terminals of small generators during the starting of relatively large motors has long been an important problem, particularly in connection with the starting of large motors on power-house auxiliary generators. Interest in this problem has been greatly accentuated by the wartime application of many small a-c generators, in fixed and mobile locations, supplying motors of ratings amounting to an appreciable fraction of the generator rating. It is essential in such applications to be able to predict accurately the maximum voltage drop in order to be sure that motor starters on the line will not drop out and that motors already running will not stall when large additional loads are started. Other types of apparatus are critical to low voltages of short duration, particularly if such low voltages recur frequently.

A practical method[1] has been available for some time for determining generator voltage–time curves following the sudden application of load. In this method the voltage–time curve is constructed by a step-by-step graphical process. In this paper equations are derived that yield directly the minimum voltage of the voltage–time curve. The calculation of a large number of specific cases using both these methods has indicated that many of the factors that enter theoretically into the problem have practically a small effect on voltage drop, and that by using average or typical values for the factors of small importance, the maximum voltage drop during motor starting can be expressed in curves in terms of only a few important parameters.

A general study has been made to determine the effects of varying independently all of the factors entering into the regulation problem. This study demonstrates conclusively that average values can be used for the less important factors. As a result it has been possible to present sets of curves that show with accuracy the maximum voltage drop to be expected when motors of any size are started on generators of various characteristics. These curves can be used without involved calculation and with a knowledge of only the most essential factors entering into the problem; namely, the full voltage inrush current of the motor, the transient reactance and the open-circuit time constant of the generator, and the response of the exciter. Tables are included to show the average values of these various constants for generators and exciters commonly used; therefore, a fairly good approximation to the maximum drop can be obtained from the information given in the paper without detailed information regarding the equipment involved.

Curves of Maximum Voltage Drop

The final results of the studies described in this paper are the maximum-voltage-drop curves, Figure 1, for suddenly applied low-power-factor load. The curves apply to a regulating system having essentially the elements shown in Figure 2. A direct-acting regulator varies resistance in the field of a self-excited exciter to regulate the a-c generator voltage. This system of regulation is the one most commonly used with a-c generators ranging in size from the smallest up to machines of several thousand kilovolt-amperes. While the curves were calculated for machines having self-excited exciters, they apply approximately in the case of large machines having separately excited exciters.

Figure 3 is a typical curve of the voltage–time behavior of a generator under suddenly applied load. The calculated points of this figure were obtained by means of equations derived in a later section of this paper. When a low-power-factor load is suddenly thrown on a generator, the terminal voltage drops suddenly as shown in Figure 3 because of internal reactance drop and then begins a gradual drop to a much lower value because of armature reaction. The initial drop initiates action of the voltage regulator, which removes resistance from the exciter field. This causes the exciter voltage to begin building up, stopping the downward trend of generator voltage and bringing this voltage back to normal. The most important single feature of this "voltage-dip" characteristic is the maximum drop reached. Entire dips involve times of 0.5 to 1.5 seconds, and the voltage is near its minimum value for several cycles. Consequently, the maximum drop, reached at the low point of a dip, is the criterion of undesired starter dropout, motor stalling, and other phenomena associated with low voltage. It is this maximum drop that has been plotted in the curves of Figure 1.

The curves of Figure 1 show the maximum voltage drop as a function of the amount of low-power-factor load added directly at the generator terminals for a wide range of values of generator transient reactance. Families of curves of this type are given for various generator time constants and exciter responses. Table I shows probable minimum, maximum, and average values of the generator reactances affecting voltage drop. Figure 4 shows typical time constants of machines of different sizes and speeds.

A summary of the basis for the curves of Figure 1 is given in Table II. Although derived on the basis of no initial load, the curves apply well for the mixed static and rotating initial loads usually encountered. The curves were calculated under the assumption of 35 per cent power factor for the applied load. This is a typical starting power factor for squirrel-cage induction motors, and as shown in a later section wide variations in power factor from this typical figure will have little effect on the maximum drop.

In the determination of maximum voltage drops from Figure 1, applied loads should be expressed in terms of per cent generator current at full voltage. In the calculation of the curves the added load was treated as a fixed impedance. The impedance of a normal induction motor is fairly constant at the locked-rotor figure at speeds up to at least 75 per cent of full speed, and a loaded induction motor will not normally reach this speed during the short time required for the regulator to stop the downward trend of generator voltage; therefore, in determining maximum drops from Figure 1, induction motors can be assumed without signifi-

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Development of the mathematical method for the determination of maximum voltage drops is abstracted from a thesis, "Suddenly Applied Loads on Generators With Quick-Response Excitation," by R. C. Cheek, submitted to the University of Pittsburgh in partial fulfillment of the requirements for the degree of master of science.

cant error to be fixed impedances during the starting period.

Example: Suppose a 50-horsepower 440-volt induction motor, having 600 per cent starting current at full voltage, is to be started across the line on a 500-kva generator of 30 per cent transient reactance. Full-load kilovolt-ampere input for a typical motor of this size is 48.2 kva. The full-voltage starting inrush is therefore $6\times 48.2=290$ kva or 58 per cent of generator rating. With a generator time constant of 2.0 seconds and an exciter response of 3.0 or higher, the maximum drop according to Figure 1 is 17.5 per cent, or in other words the minimum voltage is 82.5 per cent. This provides considerable margin above contactor-dropout voltages of 65 to 70 per cent and the 70 per cent voltage at which a fully loaded motor of 200 per cent pull-out torque would stall if it were already running on a line supplied by the generator.

Factors Influencing Voltage Regulation

The maximum voltage drops have been given as functions of four principal parameters; namely, added load, generator transient reactance, generator time constant, and exciter response. There are a number of other factors entering into the equations for maximum voltage drop, but as will be shown these are generally minor factors, which can justifiably be represented by average values in determining voltage drops.

In this section all of the various factors that affect the terminal voltage of a generator during the starting of a large

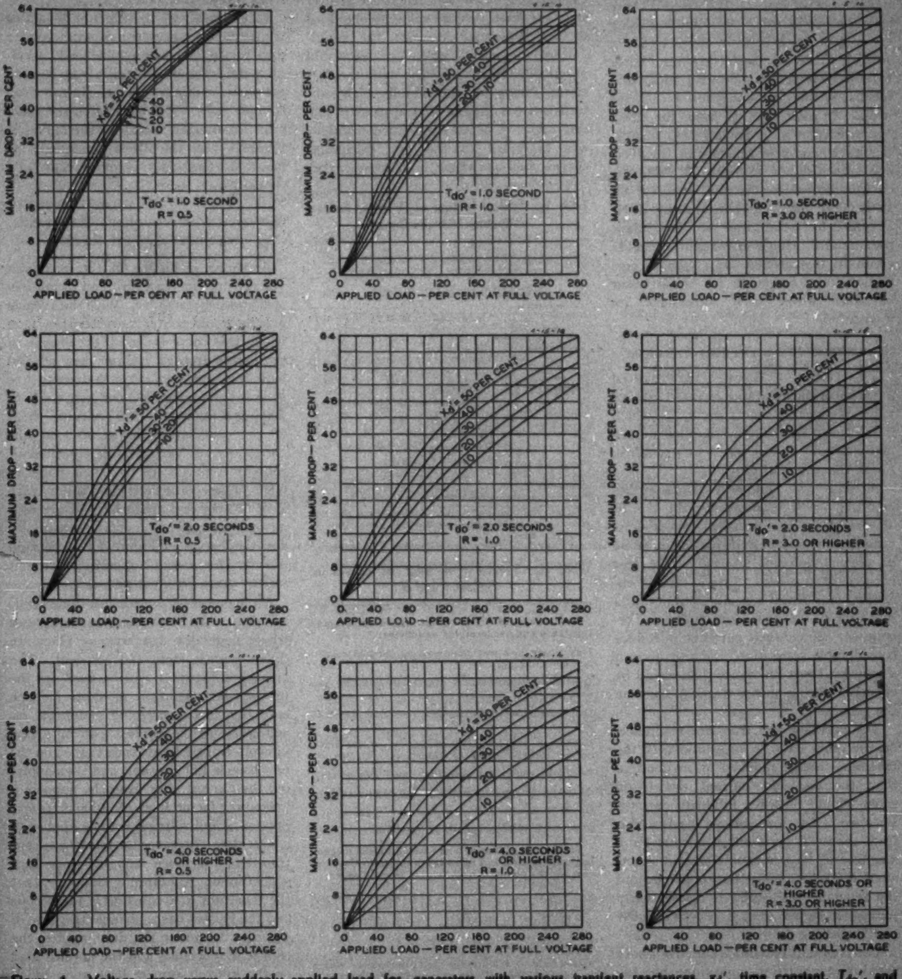


Figure 1. Voltage drop versus suddenly applied load for generators with various transient reactances, x_d', time constant, T_{do}', and nominal exciter response, R

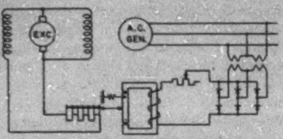


Figure 2. Essential components of excitation system with direct-acting regulator and self-excited exciter

motor or other load are discussed. These are taken up in order, consideration being given first to generator characteristics; second, to exciter characteristics; third, to voltage-regulator characteristics; and fourth, to the characteristics of the load itself. In each case, to show the effect of a particular quantity, all other quantities have been held constant at the average values shown in Table II while the one quantity under discussion was varied. In general, the voltage drop is but little affected by variations of the minor parameters. However, from the curves given in this section, approximate corrections can be made for these lesser variables where such corrections are warranted.

GENERATOR TRANSIENT REACTANCE, x_d'

The curves of Figure 1 are plotted in terms of the saturated or rated-voltage value of transient reactance, $x_{d}'_{\text{sat}}$, which is the value customarily supplied by the manufacturer as the transient reactance, x_d', of the machine.

Actually two different transient reactances are recognized by the AIEE The rated-voltage transient reactance is measured by short-circuiting the machine at normal voltage, no load. The test involves a transient current of, for example, four times normal current for a machine of 25 per cent transient reactance. Since such currents introduce considerable saturation, the test gives a somewhat lower reactance than would be effective during motor-starting duty on the generator.

The rated-current transient reactance, on the other hand, is measured with the machine voltage reduced at no load so that the transient current drawn on short circuit is just full-load current. Because of the low voltage used in this test, about 25 per cent of normal for a machine of 25 per cent transient reactance, the degree of saturation is less, and the reactance measured is consequently somewhat higher than is effective under motor-starting duty.

The transient reactance effective under usual motor-starting conditions lies between the rated-voltage and rated-current transient reactances. It has been found from test and also by analysis to be greater than the rated-voltage value by approximately one third of the difference between the rated-voltage and rated-current values. Figure 5 shows a test curve of generator voltage under suddenly applied load without regulator action. Calculated curves based on rated-voltage and rated-current values of reactance, $x_{d}'_{\text{sat}}$ and $x_{d}'_{\text{unsat}}$, show that calculations based on a reactance greater than the rated-voltage value by one third of the difference between the rated-voltage and rated-current values would practically have coincided with the test curve, except for the initially high subtransient component that appears in the test curve. The subtransient component was neglected in the two calculated curves and elsewhere in the paper because subtransient effects are normally over before maximum voltage dips occur.

Table I. Typical Values of Principal Factors Affecting Voltage Drop*

	x_d	$x_{d}'_{\text{sat}}$	x_q
Two-pole turbine generators	1.10	0.155	0.09
	0.95–1.45	0.12–0.21	0.07–0.14
Four-pole turbine generators	1.10	0.23	0.17
	1.00–1.45	0.20–0.28	0.12–0.24
Salient-pole generators	1.15	0.37	0.32
	0.60–1.45	0.20–0.50	0.17–0.40
Exciter response		1.0	
		0.5–3.0	
Regulator operating time, seconds		0.05	
		0.03–0.10	

* Reactances are per unit. Values below the line give the range of values, while those above give an average value.

Table II. Summary of Basis of Curves of Figures 1 and 6

The curves of Figure 1 are based on average constants and assumptions as follows:

Direct-axis transient reactance x_d used in calculation $= 1.07 x_{d}'_{\text{sat}}$.

Direct-axis synchronous reactance $x_d = 1.20$ per unit

Quadrature-axis synchronous reactance $x_q = 0.75$ per unit

Excitation required for saturation at no load, normal voltage —0.20 per unit

Regulator equivalent operating time—0.05 second

Added load—constant impedance, 0.35 power factor

Initial load—zero

Regulating system—shown in Figure 2

The curves of Figure 6 are based on the foregoing assumptions and in addition the following, except for the one parameter varied in each case:

$x_{d}'_{\text{sat}} = 0.25$ per unit, $x_d' = 1.07 \times x_{d}'_{\text{sat}} = 0.268$ per unit

$T_{do}' = 3.0$ seconds

Exciter response $= 1.0$

Added load—a 0.35 power-factor impedance which would draw 100 per cent of generator full-load current rating at normal voltage

The ratio of rated-current to rated-voltage transient reactance varies from 1.14 for average standard machines to 1.3 or greater for more highly saturated lightweight machines built for mobile service. Thus, "greater by one third of the difference" means from 1.04 to 1.10 times the rated-voltage value, $x_{d}'_{\text{sat}}$. The curves of Figure 1 were calculated using a reactance of 1.07 $x_{d}'_{\text{sat}}$.

Figure 6a shows the effect of generator transient reactance on maximum voltage drop for 100 per cent applied load. Doubling the transient reactance from 20 to 40 per cent multiplies the maximum drop by only 1.38. This illustrates the small error involved in the use of the curves of Figure 1 when the effective reactance for motor-starting duty is other than 1.07 times $x_{d}'_{\text{sat}}$.

GENERATOR SYNCHRONOUS REACTANCE, x_d, AND SHORT-CIRCUIT RATIO

As shown in Figure 6b, the maximum voltage drop increases with an increase in synchronous reactance. With the average generator and exciter characteristics upon which this curve is based, the change in maximum drop at 100 per cent applied load is about two parts in twenty five as x_d is varied from 120 per cent down to 100 per cent or up to 140 per cent. With 20 per cent saturation at no load, the corresponding variation in short-circuit ratio is from 1.0 up to 1.2 or down to 0.86. These variations from the average x_d of 120 per cent are as large as are usually encountered among normal machines, and the curves of Figure 1 can be used directly for normal machines without serious error for most purposes.

An accurate figure for maximum voltage drop may be required in some cases where generator synchronous reactance differs greatly from 120 per cent. The curves of Figure 1 can be used for the precise determination of drop in such cases by first expressing generator reactances and the applied load on a new kilovolt-ampere base, such that x_d on the new base is 120 per cent, and then applying the curves. For example, suppose a low-power-factor load of 1,500 kva (expressed at full voltage) is to be applied to a 3,000-kva generator having 30 per cent transient reactance and 150 per cent synchronous reactance. Suppose that the generator time constant is 4.0 seconds and the exciter has a nominal response of 1.0. To determine the drop, express the transient reactance and the applied load on the kilovolt-ampere base upon which x_d is 120 per cent. The base in this case

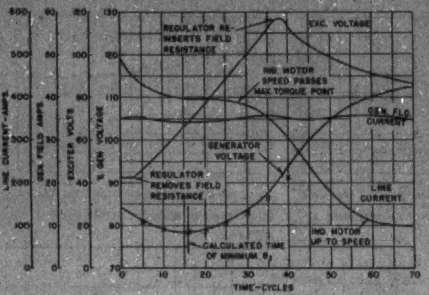


Figure 3. Typical voltage-dip curve obtained from test with calculated points (crosses) for comparison

Added load 0.97 per unit at 0.41 power factor to 375-kva generator initially operating at 0.20 per-unit load, 1.0 power factor

will be 3,000×120/150=2,400 kva. On this base the transient reactance x_d' is 30×2,400/3,000=24 per cent, and the applied load is 1,500/2,400=62.5 per cent. From the curves of Figure 1, the maximum voltage drop is 15 per cent for 62.5 per cent load applied to a generator having 24 per cent transient reactance, a time constant of 4.0 seconds, and an exciter of 1.0 nominal response. This same maximum drop would be obtained with the machine and load under consideration.

Generator Open-Circuit Time Constant, T_{do}'

The maximum drop decreases as the time constant is increased, as shown in Figure 6c. In general, the time constant is longer for larger machines, typical values being given in Table I. In view of the relatively large effect of variations in this factor, it was selected as one of the four main parameters in the calculation of Figure 1.

Generator Quadrature-Axis Synchronous Reactance, x_q, and Saturation, s

As shown in Figure 6d, variation of x_q has practically no effect on the maximum voltage drop. This effect would be expected for low-power-factor loads, because for such loads practically all the phenomena take place in the direct axis, quadrature-axis effects being relatively unimportant.

The increased excitation required for saturation in normal machines as compared with that which would be required with no saturation enters into the voltage-drop calculation. An excitation at no-load normal voltage 20 per cent greater than that required to produce normal voltage on the air-gap line was used in calculating the curves. Figure 6e shows that the drop is not altered materially by varying this factor from zero to 40 per cent. The latter figure represents a machine having above-average saturation.

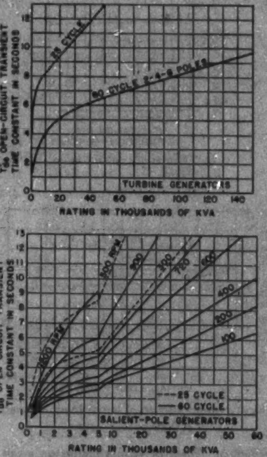


Figure 4. Typical open-circuit time constants of machines of different sizes and speeds

Exciter Response

The voltage of a self-excited exciter builds up at an accelerating rate in an exponential curve until limited by saturation. The time constant of the exponential increase is directly related to response as defined by the American Standards Association, as will be shown in a later section. Below saturation the exciter voltage increases 2.718 times per time constant.

As shown in Figure 6f, maximum generator voltage drop is reduced sharply by an increase in response up to 1.0, but beyond this point the gain is not as pronounced. The curve is based on a generator time constant of 3.0 seconds. With shorter generator time constants, increases in the exciter response above 1.0 will reduce the drop more than indicated by the curve.

Regulator Operating Time

After the voltage drops, the regulator requires a finite time to short-circuit the resistance in the exciter field circuit. While this is not an instantaneous process, an equivalent regulator operating time can be used with negligible error. Sudden short-circuiting of the resistance at the regulator operating time is equivalent to the actual gradual removal of resistance. The curves of Figure 1 are based on a "regulator operating time" of 0.05 second, a fairly representative value for the direct-acting-type regulator. As shown in Figure 6g the drop is increased only 0.7 per cent out of 25 per cent, if this time is doubled; consequently, no great error is made in using the curves of Figure 1, based on a regulator operating time of 0.05 second, for cases where operating time may be slightly greater or less than this figure.

Initial Load

The initial load on a generator influences the voltage drop when additional load is suddenly applied. As shown in Figure 6h, a static or constant-impedance initial load reduces the voltage drop caused by suddenly applied load. However, a load that draws additional current as voltage decreases may increase the voltage drop. Such loads will be referred to herein as "dynamic" loads. For example, a running induction motor may drop slightly in speed during the voltage dip so that it actually draws an increased current and thereby increases the maximum voltage drop. The dynamic initial load curve of Figure 6h is based on an initial load that draws constant kilowatts and power factor as the voltage varies.

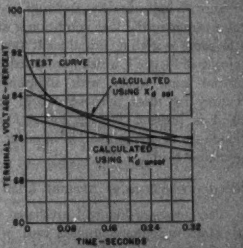
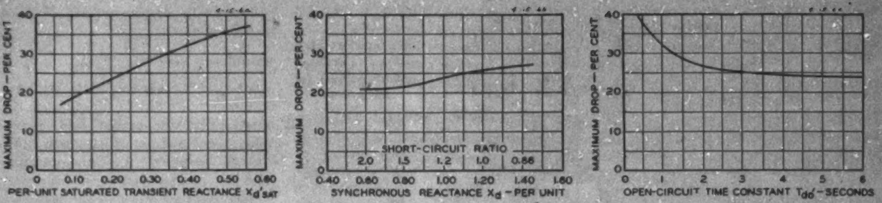


Figure 5. Test and calculated voltage–time curves of an a-c generator with a suddenly applied reactive load

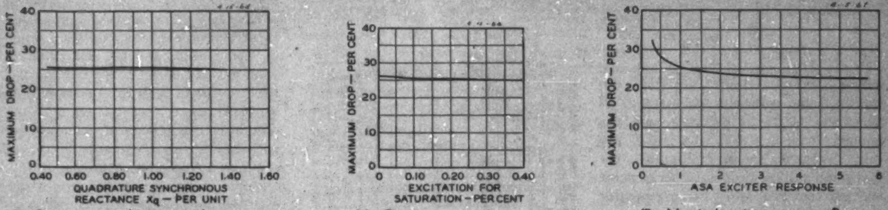
No regulator action. Subtransient effects were not considered in calculated curves



(a). Generator transient reactance, $x_d'_{sat}$

(b). Generator synchronous reactance, x_d

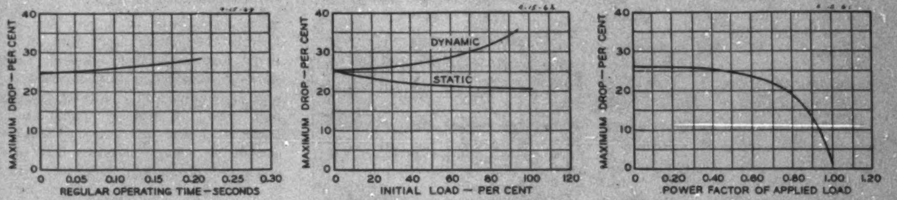
(c). Generator open-circuit time constant, T_{d0}



(d). Generator quadrature synchronous reactance, x_q

(e). Generator saturation, s

(f). Nominal exciter response, R



(g). Regulator operating time, h

(h). Type and amount of initial load, assumed at 0.80 power factor

(i). Power factor of applied load

Figure 6. Effect of various factors on maximum voltage drop when 100 per cent low-power-factor load is suddenly applied to an a-c generator

For each curve only the factor given as the abscissa was varied, other factors being kept constant at values given in Table II

An initial load is usually some combination of static and dynamic loads, and the voltage drop with additional suddenly applied load is therefore intermediate between the drop with an initial load entirely dynamic in character and the drop with entirely static initial load. Figure 6h indicates that the drop for such an average initial load condition is about the same as with no initial load; thus the curves of Figure 1, which were based on no initial load, apply with sufficient accuracy for most purposes where initial load is present, if such initial load is made up of a combination of static and dynamic load.

Characteristics of Applied Load

The curves of Figure 1 are based on a constant-impedance applied load, which as has been pointed out can be used to represent an induction motor during the starting period. In the curves the power factor of applied load was taken as 35 per cent, a typical motor-starting power factor. The maximum voltage drop is practically the same for all power factors from zero to 40 per cent but decreases rapidly for power factors above this figure, as indicated in Figure 6i.

Equations for Maximum Voltage Drop

In this section equations are derived into which machine and load quantities can be substituted to give directly the minimum voltage of the voltage–time curve of a generator under suddenly applied load. A simple mathematical expression for the build-up of voltage of a self-excited exciter is derived, based on the ASA definition of nominal response. This expression is substituted into the differential equation of generator internal voltage under closed-circuited armature conditions, and the differential equation is solved, yielding an expression for the internal voltage as a function of time. An equation for the time at which the internal voltage is at its minimum value is derived. The time of minimum voltage, determined by means of the latter equation, can be substituted into the generator-voltage equation to determine the minimum internal voltage, from which the terminal voltage can readily be determined.

Analytical Approximation of Exciter Build-Up

The nominal response of an exciter is defined as the ratio to the nominal slip-ring voltage of the slope in volts per second of the straight line, beginning at nominal slip-ring voltage on the voltage–time curve and continuing for one-half second, under which the area is the same as the area under the actual no-load build-up curve over the same half-second in-

Table III. Characteristics of Generators and Exciters Used in Tests

	Group 1	Group 2
Transient reactance, $x_{d\,\mathrm{sat}}'$	0.218 per unit	0.170 per unit
Synchronous reactance, x_d	0.932	0.715
Quadrature synchronous reactance, x_q	0.566	0.48
Open-circuit time constant T_{do}'	1.61 seconds	1.40 seconds
Excitation required for saturation at no-load normal voltage	0.183 per unit	0.312 per unit
Exciter response	2.16	2.77

terval. Nominal slip-ring voltage is the voltage required at the slip rings of the main generator to generate full load at rated power factor. An illustrative calculation of nominal exciter response is given in Figure 7.

The definition of nominal response suggests a means of approximating analytically the voltage–time curve of an exciter when the only information available about the exciter is its nominal response. In a self-excited exciter, terminal voltage builds up with an exponentially rising characteristic, following this characteristic until saturation limits it to a ceiling value. Over the unsaturated region of build-up, the voltage can be represented by an expression of the form

$$e_x = e_1 \epsilon^{t/T_e} \qquad (1)$$

in which e_1 is the initial value of e_x, that is, the voltage at $t=0$, $\epsilon=2.718$, and T_e is a time constant depending upon exciter field and armature characteristics. Equation 1 can be used to represent the effect of an actual exciter by choosing the time constant T_e so that the resulting curve has the same nominal response as the actual exciter build-up curve. Such a curve, having the same nominal response, will then follow fairly closely the actual exciter curve over the half-second range beginning at nominal slip-ring voltage, and it can be used as a good approximation for voltages below nominal slip-ring voltage. Maximum voltage dip normally occurs with small and medium sized generators within one-quarter to one second after sudden application of load, and any departure of the analytical approximation from the actual curve within such times will be negligible. Use of the nominal response, which is based on the no-load build-up curve of the exciter, to represent exciter build-up under load conditions, is a justifiable approximation for usual exciter proportions. This will be shown later by comparison of tests with calculations involving this approximation.

The curve of Figure 8 gives directly values of T_e as a function of nominal response. The derivation of this curve is given in Appendix 1. Exciter build-up can be represented by substituting into equation 1 the value of T_e from Figure 8 corresponding to the response of the actual exciter. The initial exciter voltage, determined by the excitation requirements of the main generator under load conditions preceding sudden application of additional load, should be substituted for e_1. Figure 7 gives a comparison of a typical exciter build-up curve with its analytical approximation, equation 1.

GENERATOR-FIELD-CIRCUIT EQUATION, ARMATURE OPEN-CIRCUITED

In the analysis of generator voltages and currents that follows, attention will be confined to effects in the main field and armature circuits, that is, subtransient currents in damper windings and pole faces will be neglected. The reason for this is that the time constant of decay of these subtransient effects is so short (on the order of two or three cycles) that their duration is negligible compared to the duration of the transient component. The transient time constants of normal machines and the time required for normal exciters to build up are so long by comparison that subtransient effects will have disappeared after a sudden change in the armature circuit long before the lowest point of the voltage dip is reached.

The differential equation for the field circuit of a generator with open-circuited armature is that for a simple series circuit with inductance and resistance:

$$L\frac{di_f}{dt}+ri_f=e_x \qquad (2)$$

In equation 2, L is field-circuit inductance, i_f is field current, r is field resistance, and e_x is exciter voltage, fixed or variable. If this equation is divided through by r, and L/r is denoted by T_{do}', the time constant of the field circuit when the armature is open-circuited, it can be written

$$T_{do}'\frac{di_f}{dt}+i_f=\frac{e_x}{r} \qquad (3)$$

If i_f in equation 3 is expressed as a per-unit quantity in which one unit is the amount of field current required to produce unit voltage at no load on the air-gap line of the machine, it becomes identical with the per-unit value of e_d, the direct-axis synchronous internal voltage. That is, for each per-unit of value field current there is an equal per-unit value of e_d. Likewise e_x/r can be expressed in per-unit terms where one unit is the field current required to produce unit voltage on the air-gap line. If one unit of e_x is taken as the exciter voltage required to force unit field current through the field resistance r, equation 3 can be rewritten in per-unit terms as follows:

$$T_{do}'\frac{de_d}{dt}+e_d=e_x \qquad (4)$$

FIELD-CIRCUIT EQUATION WITH CLOSED-CIRCUITED ARMATURE

When a sudden change takes place in the armature circuit of a generator, nearly all the internal voltages of the machine, including e_d, undergo an instantaneous change. The one quantity that does not change instantaneously during a transition from one operating condition to another is e_d', the direct-axis transient internal voltage. This quantity is proportional to the total flux linkages with the field winding, which cannot undergo an instantaneous change.

The fact that e_d' does not change during a transition makes it a convenient quantity to use in analyzing the effect of sudden load changes upon terminal voltage. The initial value of e_d' can be determined from the original operating conditions. This initial value can be used as a boundary condition in the solution of a differential equation for e_d' after a transition, and the resulting expression can be converted to terminal voltage by use of the new circuit constants.

Wagner[1] has given the relation between e_d and e_d' under closed-circuited armature conditions as

$$e_d=e_d'\frac{r_t^2+x_{dt}x_{qt}}{r_t^2+x_{dt}'x_{qt}} \qquad (5)$$

In equation 5 the subscript t indicates the sum of a machine quantity and an external load quantity, that is

$x_{dt}'=x_d'+x_{ext}$

$r_t=r_a+r_{ext}$

and so on.

In modifying equation 4, which applies only when the armature is open-circuited, to get an expression for closed-circuit conditions, the change in the time constant T_{do}' caused by mutual effects between the armature and field circuits must be considered. Wagner has shown[2] that under closed-circuit conditions T_{do}' can be modified to give the equivalent closed-circuit time constant T_d' as follows:

$$T_d'=\frac{r_t^2+x_{dt}'x_{qt}}{r_t^2+x_{dt}x_{qt}}T_{do}' \qquad (6)$$

Substituting the closed-circuit time constant T_d' for T_{do}' in equation 4 and substituting for e_d its value in terms of e_d' from equation 5, we get

$$T_{do}'\frac{de_d'}{dt}+\frac{T_{do}'}{T_d'}e_d'=e_x \tag{7}$$

which is the differential equation for e_d' under closed-circuited armature conditions.

SATURATION

In the derivation of equation 7 saturation was neglected and e_d was assumed to be equal under steady-state conditions to e_x. Equation 7 can be corrected for the effect of saturation by subtracting directly from e_x an amount of excitation corresponding to that used in overcoming saturation as the internal voltages of the machine vary.

In the graphical method of determining the voltage–time behavior of generators under suddenly applied load,[1] a step-by-step correction for saturation is made in the exciter voltage e_x as the voltage behind the Potier reactance of the generator varies. This correction, which is subtracted directly from e_x, is the per-unit amount of excitation required for saturation on the no-load saturation curve at voltages corresponding to instantaneous values of the Potier voltage e_p. Making the same correction in equation 7, calling the saturation component of excitation s and subtracting it from e_x we get

$$T_{do}'\frac{de_d'}{dt}+\frac{T_{do}'}{T_d'}e_d'=e_x-s \tag{8}$$

Calculation of a large number of specific cases using the graphical method indicates that over a wide range of conditions the correction s does not vary greatly from its value at no-load normal voltage. Analysis shows that this can be expected to occur for any normal range of machine constants and applied loads. For these reasons the saturation correction s has been assumed for convenience in solving equation 8 to be constant and equal to the per-unit amount of excitation required to overcome saturation at normal voltage no load. One unit of excitation, as previously noted, is the amount required to produce rated voltage on the air-gap line of the no-load saturation curve. Since the saturation correction s is a small quantity subtracted from the much larger quantity e_x, the error introduced by assuming s constant is small. The slight effect on the maximum voltage drop of varying the quantity s over a wide range is shown in Figure 6e, which further confirms the assumption that an average figure can be assumed for s without serious error.

GENERATOR VOLTAGE–TIME EQUATION

In Appendix 2, equation 8 is solved under the assumption of constant saturation, e_x being expressed in the form of equation 1. The resulting expression for the direct-axis transient internal voltage as a function of time is

$$e_d'=\left[\frac{e_1T_d'^2\epsilon^{\frac{h}{T_d'}}}{T_{do}'(T_e+T_d')}-\frac{(e_1-s)T_d'}{T_{do}'}+e_{d1}'\right]\epsilon^{-\frac{t}{T_d'}}+\left[\frac{e_1T_d'T_e}{T_{do}'(T_e+T_d')}\right]\epsilon^{\frac{t-h}{T_e}}-\frac{sT_d'}{T_{do}'} \tag{9}$$

in which e_1 is the initial exciter voltage in per unit, determined by the excitation requirements of the generator under load conditions preceding the load change; T_d' is the generator transient time constant in seconds, defined by equation 6; T_{do}' is the generator open-circuit time constant in seconds; T_e is the value read from Figure 8 corresponding to the response of the exciter; h is the equivalent regulator operating time; s is the per-unit excitation required for saturation at normal voltage on the no-load saturation curve of the generator; and e_{d1}' is the initial value of direct-axis transient internal voltage, calculated from the operating condition of the generator previous to the load change. For a generator initially operating at normal voltage, a per unit current i, and a power-factor angle ϕ, e_{d1}' in per unit will be

$$e_{d1}'=\frac{1+(x_d'+x_q)i\sin\phi+x_qx_d'i^2}{\sqrt{1+2x_qi\sin\phi+x_q^2i^2}} \tag{10}$$

For a machine operating initially at no load, $i=0$ and $e_{d1}'=1.0$. Equation 10 is derived in Appendix 3. The voltage e_d' given by equation 9 can be converted to terminal voltage e_t by the following expression.[1]

$$e_t=\frac{\sqrt{(x_{qt}^2+r_t^2)(x_{ext}^2+r_{ext}^2)}}{x_{dt}'x_{qt}+r_t^2}e_d' \tag{11}$$

Most convenient use can be made of equation 9 in determining minimum e_t by substituting in it directly the value of t at which e_d' and e_t have their minimum values. By differentiating equation 9, setting the result equal to zero, and solving for t, it can be shown that the time at which these minimum values occur is

$$t=\frac{T_eT_d'}{0.434(T_e+T_d')}\log_{10}\left[\frac{e_1T_d'\epsilon^{\frac{h}{T_d'}}-(e_1-s-T_{do}'e_{d1}'/T_d')(T_e+T_d')}{e_1T_d'\epsilon^{-\frac{h}{T_e}}}\right] \tag{12}$$

By substituting t from equation 12 into equation 9 and applying equation 11, the minimum terminal voltage e_t can be determined directly.

Experimental Confirmation of Equations

The validity of the equations just derived has been checked by oscillographic tests. Two groups of tests were made, one on each of two complete generator, exciter, and regulator setups. The combinations were in both cases identical in their essential parts to the setup shown in Figure 2.

In the first group of tests a large induction-motor starting load was applied to a 375-kva generator operating at various initial loads. The locked-rotor inrush of the motor at rated generator voltage was 363 kva, or 0.97 per unit of generator rating, at a power factor of 0.41. Exciter response was 2.16, and the operating time of the regulator was 0.05 second. Characteristics of the generator used in this group of tests are given in Table III.

A typical set of curves from an oscillogram made during this group of tests is shown in Figure 3. In this case the initial load on the generator was 0.20 per unit at unity power factor. Points from the voltage-dip curve calculated by means of equation 9 are shown for comparison. The calculated curve coincides closely with the test curve at minimum voltage and up to the time at which induction-motor speed approaches the maximum-torque point. The time of minimum voltage, determined by means of equation 12, coincides almost exactly with the actual time of minimum voltage from the oscillogram.

Figure 9 shows a representative test result from the second group of tests. In this group a 386-kva motor-starting load was applied to a 187-kva generator, an added load of 2.06 per unit. For the test shown in Figure 9, the generator was operating initially at no load. Agreement of the calculated points with test results is close up to the point at which motor impedance changes.

The two tests presented were chosen at random from the two groups of tests made. Application of the equations in the determination of maximum voltage

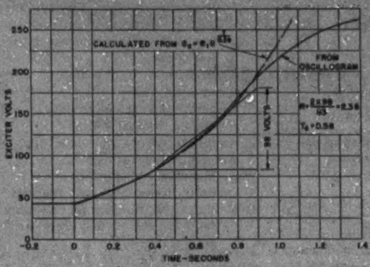
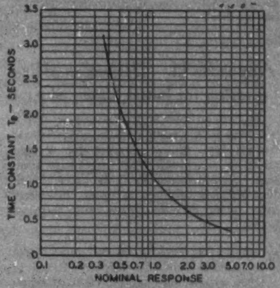


Figure 7. Voltage build-up curve of a self-excited exciter, showing method of calculating response

Nominal slip-ring voltage, 83 volts. Comparison of test curve with analytical approximation

Figure 8 (below). Time constant T_e for self-excited exciter versus nominal response



drops in other tests of these two groups as well as to several other generator setups indicates that the equations can be expected to give maximum voltage drops well within the accuracy limits usually required in engineering applications.

Conclusions

As a result of the studies described in this paper the following conclusions have been reached regarding the importance of various factors affecting voltage drop with suddenly applied low-power-factor load:

1. In the generator, the transient reactance and the open-circuit time constant are of prime importance. Reasonable variations in synchronous reactance have less effect. A method is presented of correcting for wide variations in synchronous reactance when using the voltage-drop curves. Variations in quadrature reactance and saturation have practically no effect on maximum drops.

2. For self-excited exciters the nominal response adequately expresses for practical cases the effect of exciter characteristics upon maximum voltage drops.

3. Regulator performance insofar as it affects maximum voltage drop is fully defined by the time required for the regulator to short-circuit the regulating resistance after a drop in voltage.

4. Depending upon whether it is static or dynamic in character, the initial load may reduce or increase the voltage drop slightly; consequently, the assumption of no initial load gives a good representation of average initial load conditions.

5. Variations in the power factor of the applied load from zero to 40 per cent have a negligible effect on the maximum drop. This range includes normal starting power factors for squirrel-cage induction motors, so that the curves apply for usual motor-starting problems.

It has been generally concluded that maximum voltage drops can be expressed in terms of the four main parameters, generator transient reactance, generator open-circuit time constant, exciter response, and magnitude of suddenly applied load.

Mathematical expressions for generator voltage as a function of time have been derived, based on the assumptions of constant generator saturation and the representation of exciter build-up as an exponential. These approximations are shown to be justified. The curves of voltage drop were calculated using these expressions.

Appendix I. Derivation of Curve of Exciter Time Constant T_e

From the definition of nominal response, the area in Figure 10 under the straight line between 0 and 0.5 second equals the area under the exciter build-up curve over the same period, and the slope of the straight line is the nominal response times the nominal slip-ring voltage, or Re_n. Under the assumption made in the text that exciter voltage rises exponentially, these relations can be expressed as follows:

$$\int_0^{0.5} e_n\epsilon^{t/T_e}dt = \frac{Re_n}{8}+\frac{e_n}{2} \qquad (13)$$

Integrating equation 13 and solving for R, we get

$$R = 8T_e(\epsilon^{1/2T_e}-1)-4 \qquad (14)$$

The curve of Figure 8 was plotted from the relation between R and T_e given by equation 14.

Appendix II. Solution of Differential Equation for e_d'

In the operational solution that follows, mathematical notation and the method of solution correspond to that used in reference 3. In this notation $H(t)$ represents the Heaviside unit function, and $H(t-h)$ represents the delayed unit function, that is, the function which is zero for t less than h and equal to unity for t greater than h.

The equation to be solved is equation 8. The quantity $e_x - s$ on the right-hand side must be expressed as a function of time. If the regulator operating time is called h, and if it assumed that the exciter build-up curve can be represented by the analytical approximation given by equation 1, the curve of exciter voltage after application of load will be as shown in Figure 11. The

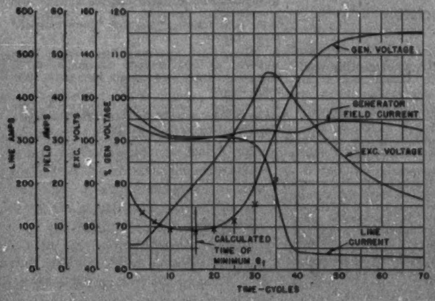


Figure 9. Comparison of calculated voltage-dip curve (crosses) with voltage-dip curve obtained from test

Added load 2.06 per unit at 0.40 power factor to 187-kva generator operating initially at no load

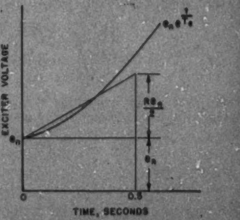


Figure 10. Derivation of relation between response and time constant T_e

Area under straight sloping line equals area under curve between 0 and 0.5 second function shown in this figure can be expressed in terms of unit functions as follows:

$$e_x = e_1 H(t) - e_1 H(t-h) + e_1 \epsilon^{\frac{t-h}{T_e}} H(t-h) \qquad (15)$$

Substituting equation 15 into equation 8, dividing by T_{do}', and multiplying s (assumed constant) by the unit function, we get

$$\frac{de_d'}{dt} + \frac{1}{T_d'} e_d' = \frac{1}{T_{do}'}\left[e_1 H(t) - e_1 H(t-h) + e_1 \epsilon^{\frac{t-h}{T_e}} H(t-h) - sH(t) \right] \qquad (16)$$

Writing equation 16 in operational form (replacing d/dt by p), adding the operational form of e_{d_1}', the initial value of e_d', and dividing by $p+1/T_d'$ we get

$$e_d' = \frac{e_1 - s}{T_{do}'\left(p+\frac{1}{T_d'}\right)} - \frac{e_1\epsilon^{-ph}}{T_{do}'\left(p+\frac{1}{T_d'}\right)} + \frac{pe_1\epsilon^{-ph}}{T_{do}'\left(p+\frac{1}{T_d'}\right)\left(p-\frac{1}{T_e}\right)} + \frac{pe_{d_1}'}{p+\frac{1}{T_d'}} \qquad (17)$$

Substituting equation 17 into the Bromwich–Wagner complex integral to get e_d' as a function of t, changing p to the complex parameter z, we have

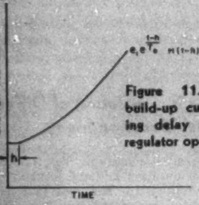


Figure 11. Exciter build-up curve, including delay caused by regulator operating time

$$e_d' = \frac{1}{2\pi i}\int_{Br_1} \frac{(e_1-s)\epsilon^{tz}dz}{T_{do}'\left(z+\frac{1}{T_d'}\right)z} - \frac{1}{2\pi i}\int_{Br_1} \frac{e_1\epsilon^{-zh}\epsilon^{tz}dz}{T_{do}'\left(z+\frac{1}{T_d'}\right)z} + \frac{1}{2\pi i}\int_{Br_1} \frac{e_1\epsilon^{-zh}\epsilon^{tz}dz}{T_{do}'\left(z+\frac{1}{T_d'}\right)\left(z-\frac{1}{T_e}\right)} + \frac{1}{2\pi i}\int_{Br_1} \frac{e_{d_1}'\epsilon^{tz}dz}{z+\frac{1}{T_d'}} \qquad (18)$$

from which, upon evaluating the complex integrals and simplifying, we obtain equation 9.

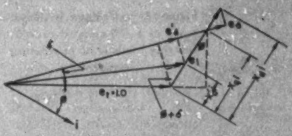


Figure 12. Two-reaction vector diagram of a salient-pole synchronous generator

Mathematically this equation is not valid for t less than h (see reference 3), but this is not important in the determination of maximum drops because the minimum value of e_d' will always occur at a value of t greater than h, that is, after the regulator has operated.

Appendix III. Derivation of Expression for Initial Value of e_d'

Figure 12 is a portion of the conventional two-reaction vector diagram for a synchronous machine. The direct-axis transient internal voltage e_d' is the projection of the transient voltage e_i upon the direct axis. That is, assuming unit terminal voltage,

$$e_d' = \cos\delta + x_d' i \sin(\phi+\delta) \qquad (19)$$

Upon expanding the last term of equation 19, we get

$$e_d' = \cos\delta + x_d' i \sin\phi\cos\delta + x_d' i \cos\phi \sin\delta \qquad (20)$$

From Figure 12

$$\operatorname{Tan}\delta = \frac{x_q i \cos\phi}{1 + x_q i \sin\phi} \qquad (21)$$

from which

$$\operatorname{Cos}\delta = \frac{1 + x_q i \sin\phi}{\sqrt{1 + 2x_q i \sin\phi + x_q^2 i^2}} \qquad (22)$$

and

$$\operatorname{Sin}\delta = \frac{x_q i \cos\phi}{\sqrt{1 + 2x_q i \sin\phi + x_q^2 i^2}} \qquad (23)$$

Substituting equations 22 and 23 into equation 20, we get equation 10.

Nomenclature

Per-unit values are used for all quantities except where specifically denoted otherwise.

$\epsilon = 2.718$
e_d = direct-axis synchronous internal voltage
e_d' = direct-axis transient internal voltage
e_{d_1}' = initial direct-axis transient internal voltage
e_i = transient internal voltage
e_N = nominal exciter voltage
e_t = generator terminal voltage
e_x = exciter terminal voltage
e_1 = initial exciter terminal voltage
h = regulator operating time
$H(t)$ = Heaviside unit function
$H(t-h)$ = delayed Heaviside unit function, that is, the function which is zero for t less than h and unity for t greater than h
i = armature current. In the Bromwich–Wagner complex integral $i = \sqrt{-1}$
i_f = field current
L = field inductance
p = parameter used in operational form of time-dependent functions
r_a = generator armature resistance
r_{ext} = external resistance, that is, load resistance at generator terminals
$r_t = r_a + r_{ext}$
R = nominal exciter response by ASA definition
s = per-unit excitation required to overcome saturation at no load, rated voltage
t = time in seconds
T_d' = short-circuit transient time constant defined by equation 6
T_{do}' = generator open-circuit transient time constant
T_e = time constant of exciter build-up
x_d = unsaturated direct-axis synchronous reactance
$x_{dt} = x_d + x_{ext}$
x_d' = direct-axis transient reactance
$x_{dt}' = x_d' + x_{ext}$
x_{ext} = external reactance, that is, load reactance at generator terminals
x_q = quadrature-axis synchronous reactance
$x_{qt} = x_q + x_{ext}$
z = complex variable parameter used in Bromwich–Wagner complex integral

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3. COMPLEX VARIABLE AND OPERATIONAL CALCULUS (book). N. W. McLachlan. The MacMillan Company, New York, N. Y., 1942.

Lightning Protection for Rotating Machines

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Synopsis: The high monetary value and low insulation levels of rotating machines make it necessary to employ special protection. A rotating machine can be conceived as a transmission line with distributed constants, the essential difference being that the machine winding is wound back on itself in the form of turns, which may permit high voltage across the turn-to-turn insulation. The installation of special arresters with low and consistent spark-over characteristics will limit satisfactorily the maximum voltage that can appear across the terminals. However, capacitors also are required which, in conjunction with the inductance in the line, slope the front of the wave and thus prevent piling up of voltage across the turns. Present methods[1–7] of protection, on the whole, have proved quite satisfactory. However, in view of the statistical data that have been accumulated in extensive lightning investigations,[8–13] the recognition of the importance of providing good grounding for the line arresters, and the appreciation of the value of the inherently low discharge voltage characteristics of the deion arrester, it has been considered desirable to study this problem further.

The more important results of this study are summarized here with the viewpoint of providing a simple and straightforward basis for machine protection which affords the increased protection and saving in installation cost and material that it was found possible to achieve. Although the protection required at the machine and in the line are mutually interdependent, it is possible to resolve the suggestions for protection into the four following categories:

A. Protection at the machine terminal or bus.

B. Line protection for machines connected directly to overhead lines.

C. Line protection for machines connected to overhead lines through cables.

D. Protection of machines connected to lines through transformers.

Protection at the Machine Terminal or Bus

FIGURE 1 lists the valve-type machine-arrester and capacitor ratings recommended for each voltage class of machine together with their method of application. It is important that the arresters at the machine be valve type. They always should be applied with capacitors that are located either reasonably close to the arresters or between the arresters and machine. This combination prevents the occurrence of steep-front surges resulting from a sudden voltage drop when the arrester discharges.

Protection of grounded machines is obtained with a minimum amount of equipment. When more than one grounded machine is connected to a bus through short feeders, good lightning protection is provided with a single set of capacitors and arresters at the bus, provided that all exposed incoming lines are connected to the bus and there is no exposed line section between the bus and a machine. If more than 500 feet of line connects a machine to the bus, it is best to provide a set of capacitors and arresters at the machine. In some cases, machine-protective apparatus may be desirable for protection against switching surges. Then it should be placed on the machine side of the machine breakers.

Experience has shown that the present practice of applying the same amount of capacitance to ungrounded machines as for grounded machines up to 6,900 volts and of using double capacitance for 11,500 to 13,800 volts in general has provided satisfactory protection. This study, however, indicates that for certain conditions more positive protection of the neutral is desirable, and it can be obtained by the addition of a capacitor and arrester in the neutral with arresters and the same capacitance as used for grounded machines at the machine terminals. The neutral arrester prevents overshooting of voltage at the neutral from reflections. The capacitor prevents a steep neutral voltage rise and a sudden drop when the neutral arrester discharges. This method of protection can be justified economically for 11,500- to 13,800-volt machines where the neutral is brought out, where mounting space is available, and where protection is applied to each machine.

Where more than one ungrounded machine is connected to the bus, one set of arresters and two sets of capacitors may be applied to each phase at the bus, or preferably one set of capacitors and arresters on each phase at the bus and one set at the neutral of each machine.

In general, ungrounded machines require different machine-protective equipment from grounded machines only when they are connected directly or through cables to overhead lines.

Line Protection for Machines Connected Directly to Overhead Lines

Line arresters must be placed far enough out on the line so that sufficient equivalent inductance is present to produce, in conjunction with the machine capacitors, the required wave sloping. Five-hundred feet of line for 650-volt machines and 1,500 feet for all higher-voltage classes were commonly used in the past. A detailed study has been made of the machine-voltage conditions that can be produced with various circuit and line-arrester grounding conditions. The lightning-current investigations[8–13] indicate that line-arrester discharge currents of 20,000 amperes provide a conservative basis for such a study. One set of valve-type line arresters, 1,500 feet out on the line, will permit too steep machine-voltage fronts for ground resistances in excess of two to three ohms when overhead ground wires are not used. The new type-*A* deion arrester with its unlimited fault-current rating, obtained without the use of a series resistance, has been found more efficient for use as the line arrester because of its very low discharge voltage. However, even with one set of these, the grounding resistance should be limited to three to five ohms.

The two methods of line protection shown in Figure 2 are recommended for providing the most effective and economical schemes for general application. The scheme of Figure 2a is recommended where the section of line within the line arresters is exposed to lightning. The multiple arrester grounds provided by the overhead ground wire permit a minimum length of 500 feet for the ground wire when the listed pole-grounding resistances can be obtained. For this length of ground wire, sufficient direct-stroke protection is obtained with a line insulation level of 250 kv. If these ground resistances cannot be obtained with driven rods, a buried counterpoise can be used. The line construction usually will be more economical, even with the counterpoise, than a longer section of ground

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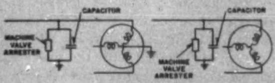
The authors acknowledge the suggestions of C. F. Wagner and J. W. Stirling in preparation of this paper.

Table I

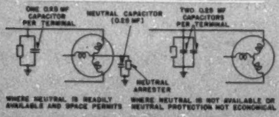
	Hydrogenerators and Other Slower-Speed Machines Machine—Kilovolt Class				Turbogenerators and Other Machines 1,800 Rpm and Above Machine—Kilovolt Class			
Transformer-Bank Connection	2.4	4.16	4.8–6.9	11.5–13.8	2.4	4.16	4.8–6.9	11.5–13.8
(*a*). Wye to delta on machine side								
Grounded systems—80 per cent arresters*	60	15	8	4	200	70	30	15
Ungrounded systems—100 per cent arresters	12	4	2	1	45	20	10	5
(*b*). Machine arresters and capacitors are recommended with wye-wye banks having both neutrals grounded								
(*c*). All other bank connections								
Grounded systems	20	6	4	2	70	30	15	7
Ungrounded systems	6	1.4	0.8	0.3	25	8	3	1.0

* Station arresters on line terminals of transformer.


CAPACITOR
MACHINE VALVE ARRESTER
CAPACITOR
MACHINE VALVE ARRESTER

(*a*). For all grounded machines and for ungrounded machines up to and including 6,900 volts. Arrester rating is determined by machine grounding conditions when mounted at machine but on system grounding when mounted at bus

Machine-Voltage Class	Surge Capacitors		Machine-Valve-Arrester Ratings (Rms-Kiovolts)	
	Micro-farads	Voltage Rating (Rms—Kilovolts)	Grounded	Un-grounded
For Grounded and Ungrounded Machines				
650	2.0	0.65	0.75	0.75
2,400	0.5	2.4	3.0	3.0
4,160	0.5	4.16	3.0	4.5
4,800	0.5	4.8	4.5	6.0
6,900	0.5	6.9	6.0	7.5
For Grounded Machines				
11,500	0.25	11.5	9.0	
13,800	0.25	13.8	12.0	


ONE 0.25 MF CAPACITOR PER TERMINAL
NEUTRAL CAPACITOR (0.25 MF)
TWO 0.25 MF CAPACITORS PER TERMINAL
NEUTRAL ARRESTER
WHERE NEUTRAL IS READILY AVAILABLE AND SPACE PERMITS
WHERE NEUTRAL IS NOT AVAILABLE OR NEUTRAL PROTECTION NOT ECONOMICAL

(*b*). For 11,500–13,800-volt machines with neutral floating, grounded through resistance or reactance exceeding 50 or 0.2 ohms, respectively, per 100 ohms of winding surge impedance. Minimum surge impedance can be taken as 100 ohms. If grounding impedance at 60 cycles is low enough, so that machine can be considered grounded from normal fault-voltage standpoint, the arresters listed under (a) may be used at the terminals, and the following may be used at the neutral:

Machine-Voltage Class	Terminal Equipment (Voltage Rating in Rms-Kilovolts)		Neutral Equipment (Voltage Rating in Rms-Kilovolts)	
	Capacitors	Arresters	Capacitors	Arresters
11,500	11.5	12	6.9	7.5
13,800	13.8	15	11.5	9.0

Figure 1. Apparatus requirements for lightning protection of rotating machines

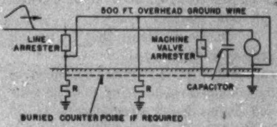
wire with higher pole-ground resistances, since an increase in line insulation level would be required also. For the case where an overhead ground wire is not used, Figure 2b, the benefit of multiple sets of line arresters arises from the fact that, for incoming surges, the farthest set of arresters discharges most of the surge current, permitting a much lower line-to-ground voltage at the inner sets.

Line Protection for Machines Connected to Overhead Lines Through Cables

Figure 3 lists the line protection recommended with cables connected between the line and machine. When line arresters are connected directly to the cable sheath through short leads, the voltage which can be transferred through the cable is only the arrester discharge voltage plus the drop across the short grounding lead. The much lower discharge voltage of deion arresters permits considerable simplification in the line protection. These schemes should be used only when deion arresters are applied at the cable pothead.

Protection of Machines Connected to Overhead Lines Through Transformers

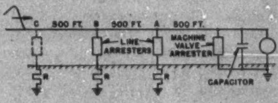
The following steps provide a method of determining the lightning protection required for machines connected to exposed overhead lines through trans-


500 FT. OVERHEAD GROUND WIRE
LINE ARRESTER
MACHINE VALVE ARRESTER
R
R
CAPACITOR
BURIED COUNTERPOISE IF REQUIRED

(*a*). Cases where overhead ground wire is required for adequate shielding

Maximum Permissible Pole-Grounding Resistance, *R*, in Ohms		
Spacing of pole grounds (feet)	250	125
Type of line arrester		
Deion	5–10	10–20
Valve	2– 5	4–10

If these resistances cannot be obtained conveniently with driven ground rods, buried counterpoise connecting line arresters to machine ground may be used.

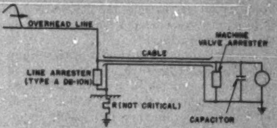

C
500 FT.
B
500 FT.
A
500 FT.
LINE ARRESTERS
MACHINE VALVE ARRESTER
R
R
R
CAPACITOR

(*b*). Cases where overhead ground wire is not used (strokes to line within 1,000 feet may endanger machine)

Type of Line Arrester	Maximum Permissible Line-Arrester Grounding Resistance, *R* in Ohms	
	Two Sets of Arresters at *A* and *B*	Three Sets of Arresters at *A*, *B*, and *C*
Deion	10–20	20–40
Valve	5–10	10–20

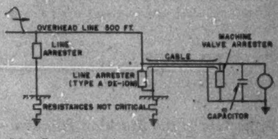
Figure 2. Recommended line-protection methods for machines connected directly to overhead lines

Machine-valve arresters and capacitors should be applied as described in Figure 1


OVERHEAD LINE
MACHINE VALVE ARRESTER
CABLE
LINE ARRESTER (TYPE A DE-ION)
R (NOT CRITICAL)
CAPACITOR

(*a*). For cables sufficiently long that section of overhead line is not required for wave sloping

Cable surge impedance in ohms	5	10	25	50
Minimum cable length in feet	2,000	1,000	500	300


OVERHEAD LINE 500 FT.
LINE ARRESTER
MACHINE VALVE ARRESTER
CABLE
LINE ARRESTER (TYPE A DE-ION)
RESISTANCES NOT CRITICAL
CAPACITOR

(*b*). For cables longer than 200 feet, but not long enough to be classified under (a). Ground wire preferred but not essential

(*c*). For shorter cables the protection methods of Figure 2 are recommended with the addition of a set of deion arresters at the cable pothead

Figure 3. Recommended line-protection methods for machines connected to overhead lines through cables

Machine-valve arresters and capacitors should be applied as described in Figure 1. Data on cable surge impedance are given in Table III. Deion arresters at cable pothead should be interconnected to cable sheath formers. Since the transformer-leakage inductance is always sufficient for wave-sloping purposes, it is not necessary to provide arresters out on the line.

(*a*). Station-type arresters should be provided on the line side of the transformer bank.

(*b*). Both capacitors and machine-valve arresters should be used when the need for either is indicated.

(*c*). When machine protection is required, only one set of standard capacitors and machine-valve arresters is required at the machine terminals, regardless of whether the machine is grounded or ungrounded.

(*d*). Machine-surge-protective apparatus should be applied to protect against surges transferred electrostatically through transformers unless 50 to 100 feet of cable, providing an effective capacitance of 0.005 microfarad or more per phase, is connected to the machine terminals. The data on cable capacitance given in Figure 10 can be used.

(*e*). Machine-protection apparatus also should be used to protect against surges transferred electromagnetically unless both of the following conditions are satisfied:

1. C_1 is equal to or greater than $15/L$

where

C_1 = positive-sequence line-to-neutral capacitance of cable in microfarads

L = leakage inductance of transformer bank in microhenrys on line-to-neutral base referred to machine side

$$L = \frac{XE^2 10^7}{377(3\phi \text{ kva})}$$

X = per cent leakage reactance of transformers

E = secondary line-to-line voltage in kilovolts

This condition assures adequate wave sloping.

2. C_1 is less than $(AL)10^{-6}$

where A has the values indicated in Table I. This condition limits the magnitude of the induced surge.

(*f*). This means of determining the use of protective apparatus is conservative. A more detailed method of calculation is given in the paper. The authors are of the opinion that in any doubtful cases protection should be applied, since the expense is small compared with the investment in machines. If machine-protection apparatus is not required for lightning surges, it still may be desirable for protection against switching surges. However, it is usually preferable to limit such surges by proper grounding methods.

Surge-Protection Levels

Consideration will be given first to the character of the voltages which it is safe to permit at machine terminals. Little direct data are available on the surge strength of the machine insulation and its variation with service. In Table II the standard AIEE crest 60-cycle voltage tests for machine line-to-ground insulation are compared with the protective characteristics of the machine-valve arresters recommended for each voltage class. That these provide safe protection has been demonstrated by ten years of field experience with many installations.

TURN-TO-TURN INSULATION

The turn-to-turn insulation of most modern large machines is designed and tested, so that each coil will stand full line-to-line rated voltage. This means that, if it is a two-turn coil, the insulation between turns will stand this full voltage. If it is a three-turn coil, it will stand one half of this voltage, and so on. However, many smaller motors and older machines were designed for only one-half this insulation strength. Most turbine generators have from one- to three-turn coils, while water-wheel generators usually have from two- to six-turn coils. Some smaller machines have as high as 40 turns per coil with 20-turn coils not uncommon. The standard practice of sloping the permissible surge voltage to a front of ten microseconds is adequate for coils with the higher number of turns. Fronts of three or four microseconds probably would not cause severe duty on one- or two-turn coils of modern large machines. However, the extent to which successive voltage impulses age machine

Table II. Protection Levels of Special Autovalve Machine Arresters

Machine Voltage Class	Standard AIEE 60-Cycle Test (Kilovolts—Crest)	Grounded Neutral Machine: Terminal Machine Arrester Rating (Kilovolts)	Grounded Neutral Machine: Gap Breakdown (Kilovolts)*	Ungrounded Machine: Terminal Machine Arrester Rating (Kilovolts)	Ungrounded Machine: Gap Breakdown (Kilovolts)*
650	3.3	0.75	2.5	0.75	2.5
2,400	8.2	3	9	3	9
4,160	13.2	3	9	4.5	13
4,800	15	4.5	13	6	17
6,900	20.9	6	17	7.5	21
11,500	33.9	9	26	12	34
13,800	40.5	12	34	15	42

* Breakdown for wave with ten-microsecond front. These values are not exceeded by the arrester discharge voltages (*RI* drop) for currents of the order of 2,000 amperes.

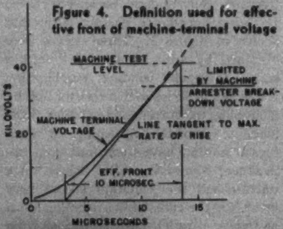


Figure 4. Definition used for effective front of machine-terminal voltage

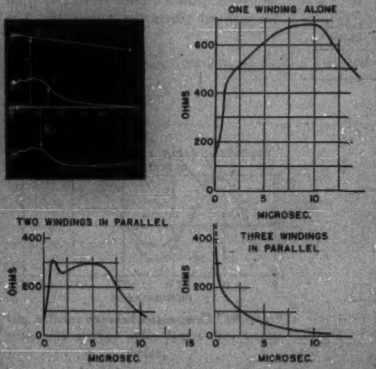


Figure 5 (left). Surge-impedance characteristics of 100-kva 2,400-volt synchronous machine used for grounding study

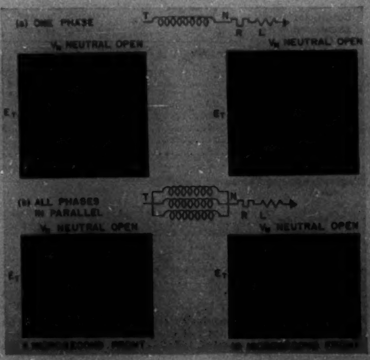


Figure 6 (right). Neutral voltage produced by various grounding resistance conditions

Impedance of the grounding resistances were as follows:

R = infinity

R = 500 ohms—L = 0.01

R = 250—L = 0.005

R = 100—L = 0.001

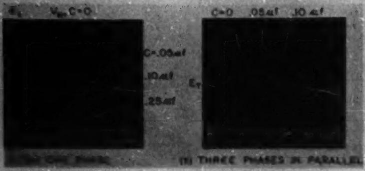


Figure 7 (left). Effect of neutral capacitance in sloping neutral voltage

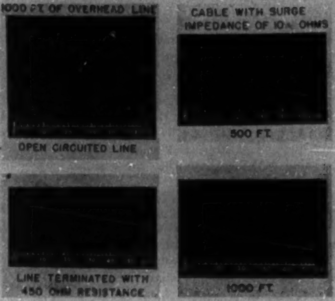


Figure 8 (right). Electrical characteristics of equivalent circuits used to represent overhead-line and cable sections

insulation in service is not clearly known. The conservative ten-microsecond front for all machines is still considered justified, not only for this reason but also to provide a uniform surge-protection basis. To obtain such a uniform basis for comparing various methods of wave sloping, the definition of the machine-terminal-voltage front given in Figure 4 is used throughout this paper. This limits the maximum voltage rise to a magnitude such that a uniform rise at this rate would not allow the test machine strength to be reached in less than ten microseconds.

Neutral Protection for Ungrounded or Impedance-Grounded Machines

The majority of machines are wye-connected. If the neutral is ungrounded or grounded through too high an impedance, reflections at the neutral for surges of the same polarity applied to two or more phases simultaneously may cause the neutral voltage to tend to double both in magnitude and rate of rise. The voltage will be highest for surges on all three phases, because the effective impedance of the three in parallel is less than for any other condition, and there is no additional phase left to act as a shunt impedance. If wave distortion and attenuation are neglected, and if the windings are considered as distortionless lines, the rate of voltage rise at the neutral of an ungrounded machine will be doubled for any incoming surge. Its magnitude will be doubled for any front less than two times the winding length, the overshoot decreasing for longer fronts. Winding lengths for a single-phase machine range from 5 to about 50 microseconds. Recommendations specifying the use of twice the machine-terminal capacitance for ungrounded machines have been based on limited tests which indicated that attenuation in the windings would prevent excessive neutral voltages if the front were increased to 15 or 20 microseconds. Whether or not this is true for all machines, the use of a neutral arrester to limit the crest voltage and a neutral capacitor to limit the rate of voltage rise provides a more positive method of protection. The neutral arrester has been used with success. It generally has been an obsolete type of gapless arrester which would not permit a sudden voltage drop following gap spark-over. An abrupt drop produces a steep-front surge propagating back through the windings which might overstress the turn-to-turn insulation. The use of conventional arresters in the neutral, therefore, has been criticized.[5] However, a capacitor in parallel eliminates this severe drop at the neutral and the terminal arresters, as pointed out by Calvert and Roman.[6]

Surge tests were made on a 2,300-volt 100-kva synchronous machine having five-turn coils and no parallel sections. Its surge-impedance characteristics are shown in Figure 5. These curves were obtained by applying a square voltage wave to each winding combination connected in series with a resistor and by measuring the current and voltages at the machine terminals. As shown, the windings at no time have a constant surge impedance; however, after about one microsecond the variation is not very great, for the cases of one winding alone or two in parallel, until reflections from the neutral reach the terminals. The

Table III. Effective Surge Impedance in Ohms of Paper-Insulated Cables*

Conductor Size (American Wire Gauge or Circular Mils)	Belted Cables* Voltage Classes 0–1 Kv	2–3 Kv	4–5 Kv	7–8 Kv	14–15 Kv	Type-H Cables 14–15 Kv
Three-Conductor Cables						
4	29	31	34	36	42	22
1	22	25	30	30	36	17
00	14	17	22	23	29	15
250,000	11	13	15	19	23	11
500,000	8	9	12	13	17	9
750,000	5	7	9	10	13	8
Single-Conductor Cables						
4	14	16	19	24	33	
1	11	12	14	20	24	
00	8	10	12	16	22	
250,000	6	8	9	13	17	
500,000	4	6	7	10	13	
750,000	5	6	6	8	11	
1,000,000	4	5	5	7	10	
1,500,000	4	4.5	4.5	6	8	

Specific inductive capacity $k = 3.7$. Velocity of propagation $v = 512$ feet per microsecond.

* Effective surge impedance for surge propagation on only one phase. This is the most severe condition. For single-conductor and type-*H* cables this is the positive-sequence surge impedance. For belted three-conductor cables, it is equal to 4/3 the positive-sequence surge impedance.

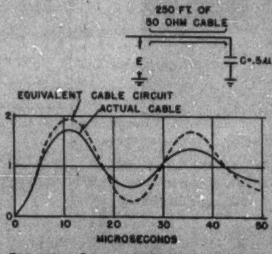


Figure 9. Comparison between solution obtained with actual cable and its equivalent circuit

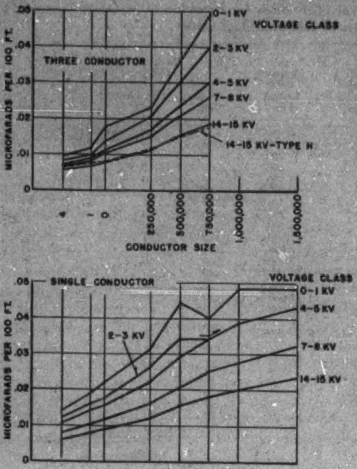


Figure 10 (left). Positive-sequence capacitance of paper-insulated cables

Three-conductor belted and type-*H* cables, and single-conductor concentric-strand cables

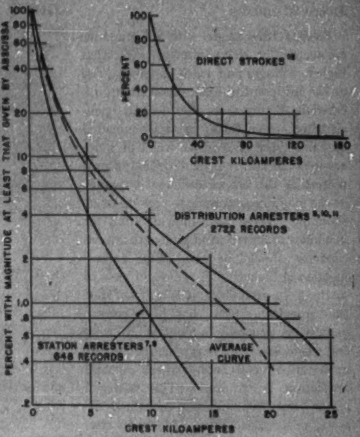


Figure 11 (right). Probability curves for crest magnitude of lightning discharge currents

electrical length for one winding is about five microseconds, and for two in parallel, it is about three microseconds. However, the three windings in parallel act more like a lumped inductance and show little distributed capacity effect. Mutual coupling between phase groups distributed around the armature induces voltages more nearly simultaneously in all coils of each phase reducing greatly the effective electrical length.

EFFECT OF GROUNDING RESISTORS

The cast grid resistors most commonly used for neutral grounding have about one-millihenry inductance for each 50 ohms of resistance at a frequency of 50,000 cycles per second, which corresponds to the surge fronts being considered. Figure 6 shows the effect of grounding impedances of this type on the neutral voltage. The five-microsecond front used for the terminal voltage E_T in the left-hand set of oscillograms is equal to the electrical length of one winding alone and more than double the effective length of the three in parallel. The terminal voltages used for the right-hand set of oscillograms has an effective front between 10 and 15 microseconds, which is about three times the length of one winding and at least six times that of the three windings in parallel. These tests, therefore, are representative of a range of conditions. Figure 6 shows that overshooting of the neutral voltage is permitted by resistors of 250 ohms or more for the three windings in parallel, even for the slower front. This indicates that 15- to 20-microsecond fronts at the terminals produce severe neutral voltages in machines with longer windings, unless there is considerably more damping than appears in this machine. Overshooting occurs for grounding resistances as low as one-half the surge impedance of one-phase winding. The lower limit of machine surge impedance is about 100 ohms. Thus, unless sufficient data are available on the surge impedance, a machine should be considered as ungrounded for any conventional resistance above 50 ohms.

GROUNDING REACTORS

Neutral voltage overshooting in this machine did not occur for inductances of less than about 2.5 millihenrys or a reactance at 60 cycles of 0.9 ohm. For the lower limit of machine surge impedance, a 60-cycle reactance of 0.2 ohm or less should not cause excessive neutral voltage. For more than 0.2-ohm reactance per 100 ohms, surge-impedance neutral protection should be provided.

Table IV. Maximum Safe Line-Arrester Ground Resistances Permitted by the Various Protection Schemes of Figure 19 Based on Protection Against 20,000-Ampere Arrester Discharge

Maximum Permissible Line-Arrester Ground Resistance in Ohms

Machine-Voltage Class	Terminal Capacitance (Microfarads)	A One Set Line Arresters 1,500 Feet Out on Line (No Ground Wire)		B Two Sets Line Arresters (No Ground Wire) See Figure 19		C Three Sets of Line Arresters (No Ground Wire) See Figure 19		D One Set Arresters Connected to Ground Wire—See Figure 19		E Two Sets of Arresters Interconnected to Ground Wire—See Figure 19	
		Valve Arresters	Deion Arresters	Valve Arresters	Deion Arresters	Valve Arresters	Deion Arresters	Valve Arresters	Deion Arresters	Valve Arresters	Deion Arresters
650	2.0	2*		5		10		5		15	
2,400	0.5	1	2.5	2.5	5	5	10	1	2.5	3.5	7
4,160	0.5	2.5	5	4	8	6	10	2	4	6	12
4,800	0.5	1	2.5	2.5	6	4	7	1	2.5	4	8
6,900	0.5	1	3	3	7	5	10	1.5	3	5	12
11,500	0.25	1	4	4	10	7	15	2.5	5	10	20
13,800	0.25	1	5	5	12	10	20	3	7	12	25

* 500 feet for 650-volt class.

Delta Machines

Similar tests made with the machine connected in delta showed somewhat higher voltages and rates of voltage rise at the midpoint of each phase than indicated in Figure 6 for the neutral open-circuited. For delta- or wye-connected machines, the neutral of which is inaccessible, the only practical method of protection is the use of sufficient capacity at the machine terminals to prevent overshooting. These tests indicate that, for machines of longer winding lengths, more capacity should be used than recommended in the past. However, in view of satisfactory experience, it is believed that the capacitance that has been recommended is generally adequate. If neutral protection can be applied, it is the most effective scheme where neutral protection is required. This material applies only to machines connected directly to overhead lines. Machines connected through transformers will be considered later.

Table V. Required Inductance of Choke Coils for Protection of Machines Connected to Overhead Lines Through Cables

Machine Voltage Class	Terminal Capacitance (Microfarads Per Phase)	Minimum Cable Length (Feet)	Choke-Coil Inductance in Microhenrys Per Phase			
			5-Ohm Cable	10-Ohm Cable	25-Ohm Cable	50-Ohm Cable
650	2.0	0*	25	25	25	25
		250	20	20	20	0
		500	15	10	0	
		1,000	10	0		
		2,000	0			
4,160	0.5	0	25	25	25	25
		250	20	10	0	0
		500	10	0		
		1,000	0			
		2,000				
2,400 4,800 6,900	0.5 for all but 6,900 ungrounded where $C=1.0$	0	50	50	50	50
		250	40	30	25	0
		500	25	20	15	
		1,000	20	10	0	
		2,000	0	0		
11,500 13,800	0.25 except for 11.5 kilovolts ungrounded where $C=0.5$	0		50	50	50
		250		35	25	15
		500		25	15	0
		1,000		10	0	
		2,000		0		
Alternative 6,900 ungrounded 11,500 ungrounded	0.50 0.25	0	75	75	75	75
		250	50	50	50	25
		500	35	30	35	10
		1,000	25	20	20	0
		2,000	15	0	0	

* Cables less than 200 feet in length.

Capacitance Required for Sloping Neutral Voltage

Figure 7 shows tests made to determine the amount of neutral capacitance required for adequate sloping. A given amount of capacitance is less effective for lower-winding surge impedances. However, 0.05 microfarad is more than sufficient for this machine. One-quarter microfarad (the lowest value now commonly used for surge-protective capacitors) should be ample for machines of lowest surge impedance. Since these are machines with one- or two-turn coils, the steeper fronts would be permissible, and this value is not critical. Calvert and Roman[6] show that about 0.175 microfarad is required to prevent a severe drop in voltage when the arrester discharges. Similar tests made in this investigation (see Figure 29) show that 0.25-microfarad capacitors are adequate. The capacity used at the machine terminals is recommended for the neutral.

Methods of Analysis

When calculating the machine-terminal voltages produced by traveling waves, analytical solutions are practical for the simpler cases in which the voltage at the line-arrester location can be expressed by a square wave, and only a continuous single section of line or cable is connected between it and the capacitor. However, for more complex cases, it is simpler and more accurate to set up equivalent circuits, produce the desired voltage or discharge current at the line arrester with a surge generator, and measure the terminal voltages with a cathode-ray oscillograph. It was found that overhead-line sections can be represented to sufficient accuracy for this study by using one equivalent Π circuit for each 125 feet. These consisted of series inductance and shunt capacitance. For cables one Π circuit was used for each 62.5 feet. The electrical characteristics of some of these circuits are shown in Figure 8. The coils for the overhead-line sections had resistance about equal to the surge resistance of an actual conductor. Low-loss condensers were used, and most of the damping was

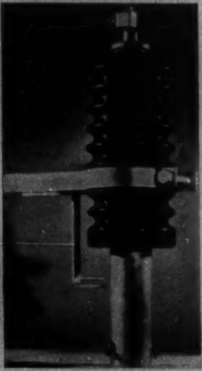
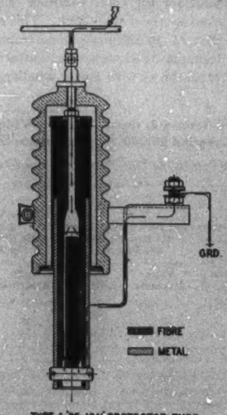


Figure 12. Nine-kilovolt type-*A* deion arrester



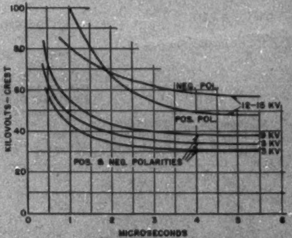


Figure 13. Spark-over characteristics of the new type-*A* deion arresters

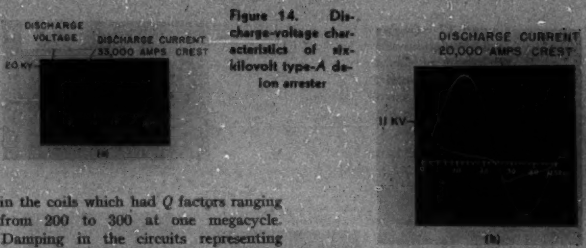


Figure 14. Discharge-voltage characteristics of six-kilovolt type-*A* de-ion arrester

in the coils which had Q factors ranging from 200 to 300 at one megacycle. Damping in the circuits representing cable sections, in most cases, was somewhat less than that of an actual cable. Figure 9 shows a comparison between the solutions obtained with an actual 50-ohm cable and its equivalent circuit. When cases with 0.25 microfarad or more connected to the machine terminals are considered, the machine does not have to be represented. Its impedance is always sufficiently high compared with that of the capacitors at the frequencies of the circuits being considered. Single-phase circuits were used for most cases, the circuit constants being chosen so as to represent the most severe condition. This is usually for a surge on only one phase, because the product of the effective machine-terminal capacitance and line or cable surge impedance is always as great or greater for surges on more than one phase than for a surge on a single phase. A surge impedance of 450 ohms was used for overhead lines.

Surge Impedance and Capacitance of Cables

For surges propagating through a circuit containing either single-conductor or type-*H* three-conductor cables having each conductor individually shielded, the positive-sequence surge impedance should be used. For other three-conductor cables, the most severe condition is given by a surge impressed on only one conductor. However, since it is electrically coupled with the other two conductors, the effective single-circuit surge impedance for this case is a function of both the positive- and zero-sequence surge impedance. For all cables of the voltage class used in machine circuits, the positive-sequence surge impedance is very nearly equal to one half of the zero-sequence surge impedance, and the effective single-circuit surge impedance is quite close to four thirds of the positive-sequence values. Table III lists the effective surge impedances of the paper-insulated cables commonly used for machine circuits. Figure 10 gives the positive-sequence capacitance of these cables.

The surge-voltage to ground at the line arresters is a governing factor in determining the machine-terminal voltages. After spark-over this is the impedance drop in the arresters and their grounds. It is also necessary to consider the voltage which can occur at the machine before the line arresters discharge or for surges propagating into the machine, the magnitudes of which are below the arrester spark-over voltage. Data, therefore, are required on the characteristics of the line arresters and the lightning currents, on which they may be called to discharge.

Characteristics of Lightning Discharge Currents

Crest Currents. Lightning current measurements have been made in several extensive investigations.[8–13] Probability curves for both distribution and station arresters are given in Figure 11, together with similar data on direct strokes. Distribution arresters are subject to a greater percentage of higher-magnitude surges. The line arresters for machine protection probably operate under conditions more closely approaching those of the station arresters. Balanced against their greater chance of receiving high-current discharges by being cut on the line is the fact that they are used on shorter lines of lower insulation than represented by Figure 11. It is thought that the average curve of Figure 11 provides a conservative basis for this application. The highest current recorded at a station arrester is about 15,000 amperes. The highest at a distribution arrester is somewhat above 30,000 amperes. These curves are based on individual arrester discharges. Data available from the three-phase station arrester studies enable a conversion of this to the probability of a disturbance (which may involve from one to all three arresters) producing a given magnitude discharge. Analysis of these data has shown that the average curve indicates a probability of about one 20,000-ampere discharge every 140 years at an arrester bank.

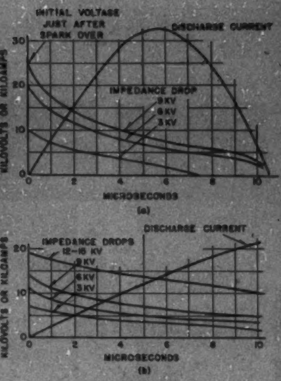


Figure 15. Discharge-voltage characteristics of type-*A* deion arresters

Wave Shape. The principal range of times to half value of arrester currents is 20 to 100 microseconds[9] with an average of 45. The maximum average rate of rise that has been recorded in the Westinghouse investigation[9] from 82 station-arrester wave-front measurements is 7,500 amperes per microsecond. After all of the factors are considered, it is believed that recommendations for the protection of important installations should be based on line-arrester discharges of 20,000 amperes with a two-microsecond effective front or an average rate of rise of 10,000 amperes per microsecond. Since consideration need be given to the machine-voltage wave shape for the first 10 or 15 microseconds only, the assump-

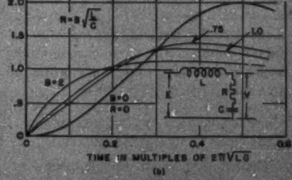


Figure 16. Curves giving general solutions for square voltage waves applied to machine surge-protection circuits

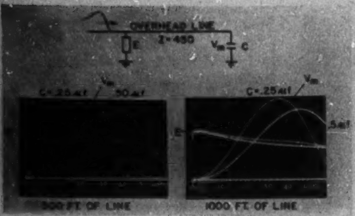
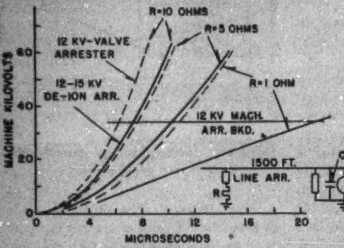
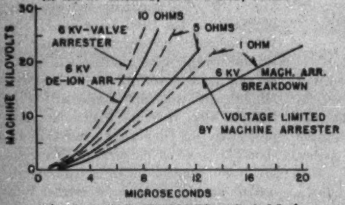


Figure 17 (left). Typical solutions of machine terminal voltage obtained with equivalent circuits



(a) 13.8 KV MACHINE, 1500 FT. LINE, C=0.25µf



(b) 6.9 KV MACHINE, 1500 FT. LINE, C=0.5µf

Figure 18 (left). Machine terminal voltages as a function of line-arrester ground resistance when one set of line arresters is used with no overhead ground wire

Based on 20,000-ampere line-arrester discharge current

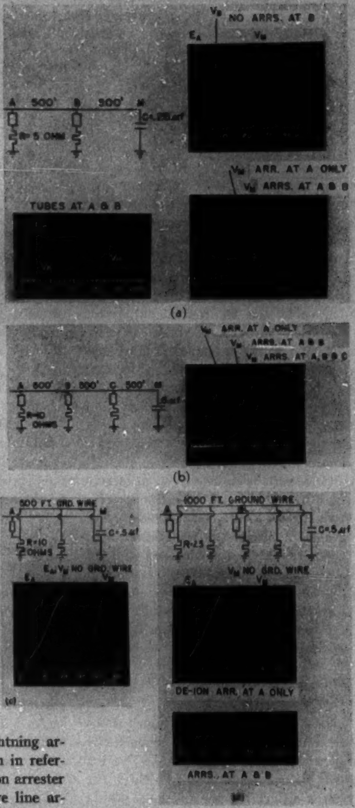


Figure 19 (right). Oscillograms showing reduction of machine voltages when multiple sets of line arresters or interconnection to overhead ground wire is provided

(a). Two sets of deion arresters with no overhead ground wire

(b). Three sets of deion arresters—no ground wire

tion of an infinite tail for the discharge current gives good accuracy and is conservative. For purposes of analysis and comparison of various protective methods, this discharge current will be used as a basis. Consideration will be given then to the variation in protective methods justified for cases of less importance or where lightning conditions might not be so severe. It will be shown that, for the methods of protection recommended in this paper for machines connected directly or through cables to overhead lines, higher discharge currents would only have the effect of shortening somewhat the front of the machine-terminal voltage. The crest magnitude, which is more important, would be limited to a safe value.

Characteristics of Protective Devices for Line Application

Distribution valve-type lightning arresters have been used mostly for the line arresters of machine-protection circuits. Average characteristics for these, as compiled by the AIEE lightning arrester subcommittee, are given in reference 14. The new type-*A* deion arrester (Figure 12) is a more effective line arrester for this particular application than are the valve arresters. Although its spark-over voltage (Figure 13) on steep-front surges is slightly higher than that of the valve arrester, the voltage across its terminals during discharge collapses quickly to relatively low values, as shown by the impedance-drop curves of Figures 14 and 15. Consequently, this deion arrester will transmit less surge energy to the machine capacitor, thereby improving the protection to the machine. Likewise, conditions at the line arrester may be more severe without impairing the degree of protection. For example, the impedance drop in the six-kilovolt deion arrester (Figure 15a) for a rate of current rise of 9,000 amperes per microsecond is 20 kv just after spark-over, but drops to about six kilovolts at the peak of the discharge. It will be shown that, as far as charging the machine capacitor is concerned, the effective impedance drop of this arrester is an equivalent square-topped voltage wave of not more than ten kilovolts, as compared with an *IR* (current-resistance) drop of about 30–34 kv for a valve arrester at the same discharge current. For a discharge current of 1,500 amperes, the equivalent square-topped voltage for the tube will range from one to four kilovolts for the 3- to 15-kv arresters. This deion arrester has an unlimited current-interrupting capacity without a series resistance which would produce a much higher discharge voltage.

Voltage Conditions Before Line Flashover or Arrester Breakdown

Before flashover occurs or a line arrester discharges, the machine-terminal voltage is determined by a wave propa-

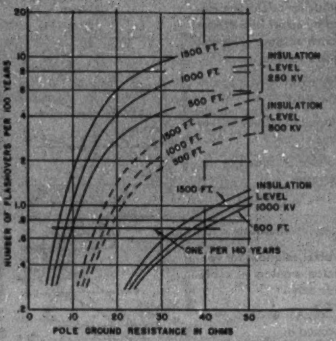


Figure 20 (left). Efficiency of overhead ground wire of various lengths as function of line insulation and pole-ground resistance

Based on density of one stroke per mile of line per year, direct-stroke-current probability curve of Figure 11, and current-wave-shape data of reference 17

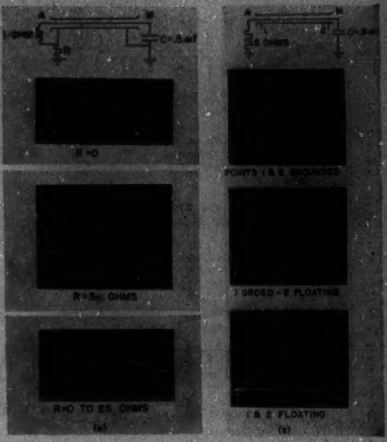


Figure 21 (right). Effect of sheath grounding on surges transferred through cables

gating in from a great distance. Figure 16a gives such a solution for a square wave. It can be seen readily that, unless a reflection point is established by flashover close to the machine terminals, no severe voltage condition will be permitted by the deion arresters. For instance, the maximum square-wave voltage that the 12–15-kv arrester will permit is 57 kv for negative polarity. Figure 16a shows that, for $C=0.25$ microfarad, the machine voltage produced in ten microseconds by this wave could be only about ten kilovolts, which is sufficient sloping even for a 2,400-volt machine.

Voltage Conditions Produced by Flashover and Arrester Discharge

Strokes producing line flashover establish a point of reflection. During the period before a line arrester discharges, the machine-terminal voltage is determined by the voltage maintained at the point of flashover and the circuit constants between this point and the capacitor. After a line arrester discharges, the machine-terminal voltage can be determined from the line-arrester location and voltage. It was found that, in practically all cases where a stroke produced flashover beyond the line arresters, the machine-terminal voltage before the line arresters discharge is insignificant. Strokes occurring to an unshielded line within the line arresters are likely to cause flashover so close to the machine that insufficient line inductance is available for wave sloping. Also, severe current duty is likely to be imposed on the machine arresters resulting in too high discharge voltages. Adequate protection from this can be secured only by providing adequate shielding, and the probability of such direct strokes, therefore, must be considered. The machine voltage produced by the voltage main-

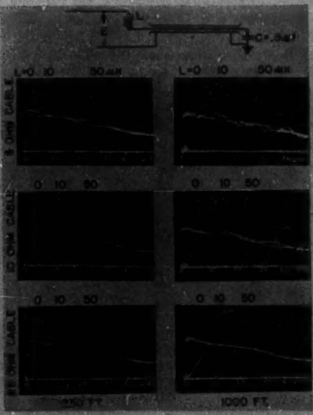


Figure 22 (left). Typical solutions for machine voltage obtained with equivalent circuits for cables of various lengths and surge impedances, and for a range of choke-coil inductances

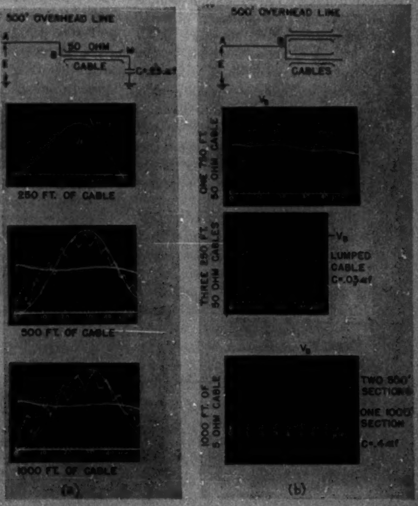


Figure 23 (right). Typical solutions for a range of cable-circuit conditions

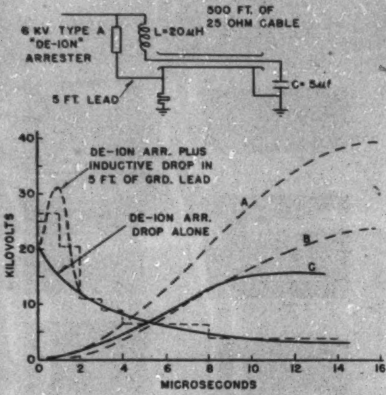


Figure 24 (left). Method for calculating machine voltage produced by actual line-arrester voltage from solutions such as those of Figures 22 and 23 for square voltage waves

A. Machine voltage for a 20,000-volt square wave at arrester

B. Machine voltage for a 12,000-volt square wave

C. Actual machine voltage permitted by the deion arrester

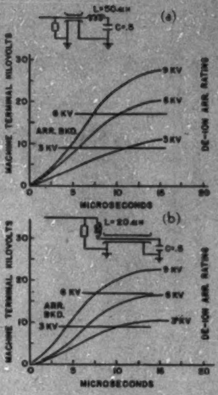


Figure 25 (right). Typical solutions for voltages permitted by deion arresters at cable potheads only

Arrester discharge of 20,000 amperes with two-microsecond front

(a). Very short cables

(b). Cable — 500 feet, 25 ohms

tained at the line arresters[5] after they spark-over will be the principal consideration in this analysis. The other conditions will be considered where they are important.

For circuits representing one set of line arresters and only a single section of overhead line between the arresters and machine capacitors, the line can be represented by a lumped inductance. The effect of the line capacitance is negligible compared with a machine-terminal capacitance of 0.25 microfarad or more. The general solution for a square voltage maintained at the end of an inductance connected to a capacitance is given in Figure 16b by the curve for R equal zero. Typical solutions obtained with the equivalent circuits are shown in Figure 17. These solutions cannot be distinguished from those calculated by the curve of Figure 16b. For most of the more complex cases considered, a solution was obtained with the equivalent circuits and a square voltage across the line arresters. The solution for the actual voltage that would be produced across the line arrester, its lead inductance, and its ground resistance then was determined by the superposition method applying a series of square waves to approximate the actual line-arrester voltage. An example of this is shown in Figure 24.

Machines Connected Directly to Overhead Lines

Two general methods are available for obtaining the necessary inductance for wave sloping. One is to provide a set of choke coils between the line and machine capacitors with line arresters on the line side of the coils. The other is the use of the line inductance. Several factors should be considered in comparing these two methods. Choke coils have the advantage that a ground wire is not required to insure against direct strokes to the line section. Also, the line arresters can be placed at the machine bus and their grounds interconnected with the machine grounds to eliminate the arrester-ground resistance drop. On the other hand, the coils must be designed for adequate short-circuit and continuous current conditions. Their necessary size and construction features frequently have made them difficult to apply.

CHOKE COIL—CAPACITOR METHOD

A comparison was made of the necessary inductances and capacitances required when either valve or deion arresters are used as the line arrester. It was found that, if two of the standard capacitor units listed in Figure 1 are used at each machine terminal, the necessary choke-coil inductance is 175 microhenrys per phase when valve arresters are used as the line arresters. When type-*A* deion arresters are used, the inductance can be reduced to about 90 microhenrys.

METHOD USING LINE SECTION FOR WAVE SLOPING

One Set of Arresters 1,500 Feet Out on Line—No Ground Wire. Calculations of the machine voltages produced by the 20,000-ampere-arrester discharge with two-microsecond front have been made for each voltage class and a range of grounding resistances. Several of these

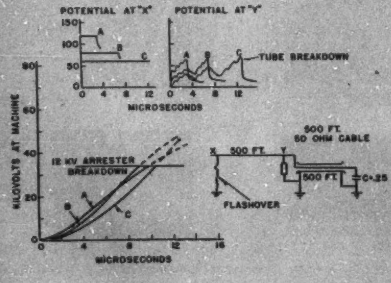


Figure 26. Typical calculation of voltages produced by line flashover 500 feet out on overhead line

Potential at *Y* determined from Figure 23a

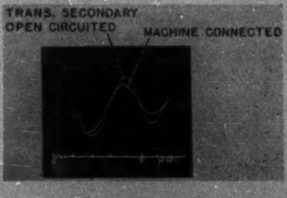


Figure 27. Comparison of voltage transferred through transformer bank when secondary winding is open-circuited and connected to rotating machine

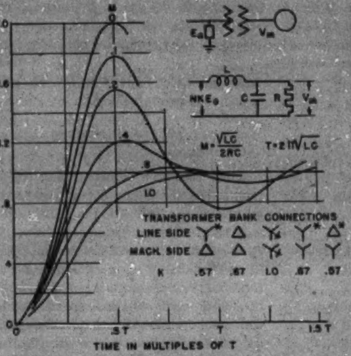


Figure 28. Equivalent circuit and solutions for determining the machine terminal voltages transferred electromagnetically through transformer banks

E_a = station-arrester line-to-ground discharge voltage

N = ratio of machine to line phase-to-phase rated voltage

L = leakage inductance of transformer bank in microhenrys on line-to-neutral base referred to machine side

C_1 = positive-sequence line-to-neutral capacitance in microfarads of cables or machine-protective capacitors connected to machine side of transformer bank

R = effective resistance in ohms given by paralleling surge impedances of all machines connected to transformer bank

V_m = machine terminal surge voltage   *Applies to grounded or ungrounded wye

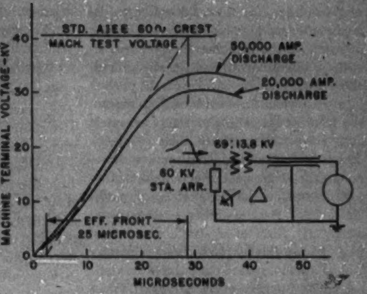


Figure 29. Machine terminal lightning surge voltage as calculated from Figure 28

10,000-kva 13.8-kv machine (surge impedance R = 500 ohms). One hundred feet of 750,000-circular-mil type-H cable between transformers and machine (from Figure 10, C_1 = 0.0175 microfarad). Transformer leakage reactance = 8.5 per cent (L = 4,200 microhenrys). Station arrester E_a = 234 kv at 20,000 amperes and 260 kv at 50,000 amperes. From Figure 28, $0.5T$ = 26 microseconds, M = 0.47, $NKE_a = 0.2 \times 0.57E_a = 0.114E_a$

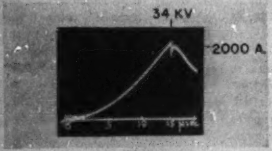
are shown in Figure 18. For this case, 30-foot line-arrester ground leads were assumed. As shown by Figure 18, the machine-terminal voltage in most cases is too steep when valve arresters are used with a ground resistance above one ohm. For deion arresters the permissible resistance ranges from 2.5 to 5 ohms. Even this resistance is frequently difficult to obtain.

Multiple Sets of Line Arresters—No Ground Wire. The high rates of machine-voltage rise permitted by one set of line arresters with high ground resistance can be reduced very effectively by the use of more than one set spaced at intervals along the line. This is illustrated in Figures 19a and 19b for cases where deion arresters are used. The benefit results from the fact that initially practically all of the surge current will be discharged by the arrester farthest out on the line. The percentage of current which initially flows past this point to the next arrester set depends on the ratio of the arrester ground resistance to the line surge impedance. The rate at which it increases depends on this and the distance between the arresters. It was found that the optimum distance between arrester sets for this application is about 500 feet. For the test shown in this figure, the discharge current flowing into the point *A* had a 70-microsecond tail. As shown by the left-hand oscillogram of Figure 19a, when two arrester sets are used, the voltage at the second arrester builds up slowly and does not exceed about one fourth of the maximum voltage across the first arrester. The machine-terminal voltage is also reduced to about one fourth of the magnitude permitted by one set of arresters at *A* only. Figure 19b shows the added benefit of a third set of arresters 1,500 feet out on the line.

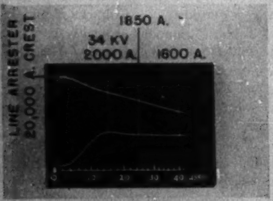
In column *B* of Table IV are listed the grounding resistances permitted by two sets of arresters. When valve arresters are used, the range is 2.5 to 5 ohms. For deion arresters, it is 5 to 12 ohms. Column *C* gives similar data for three sets where the corresponding ranges are 5 to 10 ohms and 10 to 20 ohms, respectively. In determining these values, consideration was given also to the current surges of lower magnitude which would not discharge the inner sets of arresters. The most severe voltages that can be produced under these conditions are about equal to that set up by the 20,000-ampere surge.

Benefits of Interconnection With Overhead Ground Wire. Aside from shielding, the interconnection of grounds provided by a ground wire is of considerable benefit. This is illustrated in Figures 19c and 19d for ground-wire lengths of 500 and 1,000 feet. For these two cases, pole-ground leads were spaced at 250-foot intervals providing about the minimum number of grounds that would be used normally. The shorter the ground wire the greater the benefit it provides in interconnecting the line arresters to the machine or bus ground. Balanced against this, however, is the steeper front resulting from shorter lengths of phase conductors. The minimum distance found practicable is about 500 feet. As shown by Figure 19c, the voltage and current at the line arrester has been reduced greatly after about 12 microseconds for the 500-foot case. Thus, the benefit of the ground wire is considerable. Columns *D* and *E* of Table IV show the ground resistances permitted for these two cases. The 500-foot ground wire permits resistances only about 25 per cent below those of column *B*, where two sets of line arresters and 1,000 feet of line are used. When the interval between pole grounds is cut in half, the permissible grounding resistances can be about doubled, thus allowing an upper limit of about 15 ohms for the 500-foot ground wire and 50 ohms for the 1,000-foot ground wire when deion arresters are used.

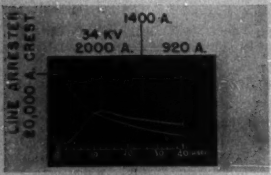
Probability of Direct Strokes Within Line Arresters. The exposure of this section of line to direct strokes will vary widely for different cases. It usually will be less than the average for the total line, since there frequently are shielding structures, such as buildings and other lines near by. However, the only direct data available on the frequency of direct strokes pertain to whole lines. The most complete data are those of Waldorf[15] for 66- to 220-kv circuits, which indicate a range of 50 to 250 strokes per 100 miles of line per year with a general average of 100 for isokeraunic levels between 25 and 40. Limited data obtained on 33-kv wood-pole lines indicate the same general average. For estimating purposes this is thought to be applicable to exposed lines of the voltage classes considered here. It is probably too high for the section of line adjacent to the machine station in most cases. When the value of 100 strokes per 100 miles of line per year is used, the probability of direct strokes occurring within the farthest set of line arresters is about once every ten years for each 500 feet. The shortest practical length of line when no overhead ground wire is used is the 1,000 feet for the case of column *B*, Table IV. The possibility of 20 strokes every 100 years or a stroke every five years within this length is thus indicated. This is much greater than the estimated probability of once every 140 years for a 20,000-ampere discharge through the line arrester. Even if the estimate of direct strokes were high by a factor of 10 or 20, the overhead ground wire should be used to equalize the two probabilities.



(a). Discharge voltage of actual 12-kv machine arrester. Parallel capacitance = 0.25 microfarad



(b). Discharge voltage and current for 1,500-foot line and 10 ohms ground resistance at line arresters. C = 0.25 microfarad



(c). Discharge voltage and current for 500-foot ground wire interconnected to line arresters. 10 ohms ground resistance. C = 0.25 microfarad

Figure 30. Typical machine-arrester discharge current and voltage obtained with model circuits (b) and (c) as compared with characteristics of actual arrester (a)

Recommended Overhead Ground-Wire Construction. The four principal construction factors determining the efficiency of overhead ground wires are the shielding angle, the insulation level of the line, the pole ground resistances, and the distance between pole grounds. The shielding angle should not be greater than 30 to 40 degrees.[16] Calculated line flashover performance, based on the direct-stroke current data now available,[17] is shown in Figure 20. Part of the analysis was made analytically and part obtained with the model circuits. On the figure is also drawn a line corresponding to a probability of flashover of once every 140 years. This is the degree of protection being considered for strokes originating beyond the line arresters and, therefore, the degree of protection that should be provided by the ground wire. For the 500-foot ground wire where column *D* of Table IV shows a maximum permissible ground resistance of seven ohms, Figure 20 indicates a line-insulation level of 250 kv to be sufficient. If the length between pole grounds is only 125 feet, this is adequate for resistances up to about 15 ohms. When 1,000 feet of ground wire are required for resistances up to 25 ohms with 250-foot pole-ground intervals, or 50 ohms with 125-foot intervals, an insulation level of about 750 kv is indicated.

It should be emphasized that the grounding resistances in Table IV are not intended to indicate a precise set of rules for protection design. The individual values of maximum resistance for each machine-voltage class were tabulated to afford a basis for comparing the various protective schemes being considered. Both the 20,000-ampere arrester discharge and the 100 strokes per 100 miles of line for direct-stroke density are considered to be conservative amply to allow a variation of two to one in the permissible ground resistances. For more important installations in exposed regions, the tendency should be to approximate the listed values. When a comparison is made of the cost of the various elements of a line, it will be found usually that, if a ground wire is used, a 500-foot section is most economical. The shorter length of overhead ground wire, fewer pole grounds, and lower required line insulation usually will exceed greatly the required added cost of obtaining sufficiently low ground resistances. Probably, in most cases, it even will permit the installation of a buried counterpoise connecting the line-arrester ground with the machine ground. This will provide adequate protection for practically any grounding condition.

Machines Connected to Overhead Lines Through Cables

Effect of Sheath Grounding Conditions

There has been some question in the past as to the effect of various sheath grounding conditions on the voltages that will be transferred through cables. To study this, the tests illustrated in Figure 21 were made. The most significant are those of Figure 21a. At one end, the conductor and sheath were connected through a one-ohm resistor and the sheath grounded through a resistor varying from zero to 25 ohms. A constant current surge was introduced at this end for the various values of resistance, thereby applying a constant surge potential between the conductor and sheath, while successively higher sheath potentials resulted as the variable resistor was increased. These tests show that the potential applied to the sheath has little effect as long as the potential between conductor and sheath does not increase, and the machine end of the cable is grounded. It is the conductor-to-sheath potential that determines the voltage transferred through the cable. Figure 21b shows that, for any other condition of grounding, the transferred voltage is essentially determined by the line-to-ground potential introduced into the cable. When the arrester grounds are interconnected at the cable pothead, the transferred surge is determined by the voltage across the arrester and its short ground leads. Cables, therefore, provide a definite voltage-limiting benefit which is greatest for deion arresters with their low discharge voltages.

Cable Circuits Studied. Three principal types of cable conditions were studied for which typical solutions obtained with the equivalent circuits are shown in Figures 22 and 23. To determine the protection afforded with line arresters at the cable pothead only, the solutions of Figure 22 were obtained for a range of cable surge impedances and lengths, and various amounts of terminal capacity and lumped inductance at the line potheads. Studies also were made (Figure 23a) of the machine conditions produced by voltages maintained various distances out on an overhead line. This is typical of line flashover or of the discharge of line arresters out on the line. The effect of various amounts of cable in parallel with machines is shown in Figure 23b.

Protection With Deion Arresters at Cable Pothead Only. It can be seen readily from Figure 22 that the voltages permitted by valve arresters will produce too steep fronts at machines for the more common cable lengths, unless fairly large choke coils are provided. It was found that they should be of the order of 75 to 100 microhenrys for cables over 500 feet in length and as high as 300 for shorter cables. However, the deion arresters permit much lower values of inductance. In some cases none is required. Typical calculations of the voltage permitted by a deion arrester with a five-foot ground lead to the cable sheath are shown in Figures 24 and 25. For lengths less than about 200 feet, the cable itself provides little sloping and acts only as a voltage-limiting device. The location of the choke coils is not important. However, for lengths greater than 200 feet, sufficient increased wave sloping is provided by the cable capacitance to warrant placing them at the line end.

Table V lists rounded-off conservative values for the required choke-coil inductance when deion arresters are used only at the line pothead of cables connected to exposed overhead lines. These are much smaller than required when cables are not in the circuit. The largest needed is 50 microhenrys, if two standard capacitors are used for 6,900- and 11,500-volt ungrounded machines. Table V is based on the reasonable assumption that a direct stroke within 500 feet of the cable pothead will cause the deion arresters to discharge. Figure 26 illustrates calculations made to show that a stroke causing line flashover 500 feet away will not cause a severe machine-voltage front. Three flashover-voltage conditions marked *A*, *B*, and *C* were considered, causing either no discharge of the arresters or spark-over after various time intervals. The wave shape of the arrester voltage was obtained from the oscillograms of Figure 23a.

Use of Line Section Instead of Choke Coils. Past practice has been to treat machines connected to overhead lines through cables the same as without cables, except for the addition of arresters at the cable pothead; these are required for cable protection. If valve-type arresters are used at the cable pothead, it is still recommended that this procedure be followed. However, as shown in Table V, deion arresters at the cable pothead only are sufficient for certain cable lengths. If, for the other cases, a section of line (with additional arresters out on the line) is used instead of the choke coils, more inductance is required. This is because interconnection of these line arresters with the cable cannot be provided through short leads. However, the line-protection schemes found necessary for machines directly connected to overhead lines are not required. It was found that, for cables longer than 200 feet, a single additional set of arresters, 500 feet out on the line and having grounding resistances even as high as 50 to 75 ohms, provides good protection. This amount of line inductance is sufficient for any surge not discharging the pothead arresters. For more severe surges the pothead-arrester discharge current will be limited to a relatively low rate of rise by the line arresters. The type-*A* deion-arrester discharge voltages are much lower for currents having low rates of rise. The discharge conditions of Figure 15b are probably considerably more severe than any that could be propagated past the line arresters. Calculations, such as those of Figure 24, showed that good protection was provided for any cable longer than 200 feet, even with pothead-arrester discharge currents, such as that of Figure 15b. Cases with cables shorter than 200 feet should be treated as if the cables were not present with the addition of pothead arresters.

Cables in Parallel With Machines. Cables at the machine or bus terminals in parallel with the circuit into which a surge is propagating were found to produce practically the same sloping as an equivalent amount of lumped capacitance. As shown by Figure 23b, the superimposed oscillations are less severe for a larger number of shorter cables or for cables of lower surge impedance. However, if the cables provide a capacitance of 0.2 microfarad or more, the pulses will not be dangerously steep. Thus, if sufficient capacitance is provided in this way, surge-protective capacitors are not required. One condition which it is important to avoid is that illustrated by Figure 23a for a machine at point *B*. If capacitors are provided, they should be so placed that the surges will reach them before the machine, so that severe oscillations which can appear on the line side of the capacitors will not be impressed on the machine.

Machines Connected to Overhead Lines Through Transformers

The mechanism by which surges are transferred through transformers has been treated in detail elsewhere.[18,19] It will be considered here only to the extent of establishing a basis for machine protection. Since the transformer-leakage inductance is always sufficient for wave-sloping purposes, it is not necessary to provide arresters out on the line. Station-type arresters, however, should be provided on the line side of the transformer bank connected to very low station grounds and preferably interconnected to the machine grounds. The surge-voltage conditions are determined by the station arresters. Average data on the discharge-voltage characteristics of the station-type arresters commonly used for transformer protection are given in reference 20. For 33 kv or higher, the arrester discharge voltage is sufficiently high compared with the short-duration lead-inductive drops which might occur, so that only the discharge voltage need be considered. It can be represented as a square voltage wave. This, of course, assumes that low station ground resistance or interconnection with the machine grounds is provided. For a current of 20,000 amperes, the arrester discharge voltage is about 2.5 times the crest-rated line-to-line voltage when 80 per cent arresters are used on grounded systems. For ungrounded systems using 100 per cent arresters it is about 3.1. Even for abnormally high discharge currents of 50,000 amperes, these values are increased only to 2.7 and 3.4, respectively. When a steep-front surge is applied to a transformer bank, voltage can be transferred electrostatically through the distributed capacitance between windings and also electromagnetically. The voltages transferred in these two ways can be considered separately.

ELECTROSTATIC COMPONENT

The electrostatic component is dependent on not only the total capacitances between windings and to ground but also their distribution along the windings. In general, therefore, it is not practical to consider individual cases. A study was made of a representative group of shell- and core-type transformers, and this was co-ordinated with the tests reported by Bellaschi.[19] With the secondary winding of transformer banks open or connected to impedances of several-hundred ohms, the electrostatic component can produce surges of severe magnitudes and fronts that usually range from 0.5 to three microseconds. However, in any transformer bank it can be reduced sufficiently for machine-protection purposes if resistances of 50 ohms or less or capacitances of 0.005 microfarad or more are connected across the secondary terminals.

Since the total capacity of machine windings usually ranges between 0.02 and 0.6 microfarad, it might be thought that the machine itself would eliminate this component. However, actual tests made by applying surges to transformer banks connected to the 100-kva machine described show that this is not so. The transformers used had lower capacities compared with capacities of the machine than is usually typical. However, tests such as that of Figure 27 show that the machine acts only as a surge impedance or resistance and has little effect on the electrostatic component. Except when surge-protection capacitors are applied, the only circuit elements commonly present to eliminate the electrostatic component are cables. The data of Figure 10 can be used to determine the capacitance provided by these. A 50-foot length is all that is required for most cables to supply the necessary 0.005 microfarad. Thus, if such cables are connected either between the machine and transformer or in parallel with the machine, special surge-protection capacitors are not required for the electrostatic component.

ELECTROMAGNETIC COMPONENT

With sufficient capacitance in the circuit to eliminate the electrostatic component, the electromagnetic component can be determined quite accurately from the equivalent circuit and solutions of Figure 28. A complete tabulation of transformer-leakage reactances (including autotransformers) is given in reference 21. Three-winding transformers can be treated accurately in terms of their equivalent leakage-reactance circuit.[31] However, a safe approximation results from considering the tertiary windings open-circuited and treating them as two-winding transformers. The constant K, determining the effective applied surge voltage for different bank connections, is discussed in detail in reference 18. It gives the highest machine voltages which can be produced. This is usually from surges on one or two phases. An example of the use of Figure 28 in calculating machine lightning-surge voltages is given in Figure 29. After M is evaluated, the shape of the voltage curve can be determined from the curves of Figure 28. The proper time scale is supplied from T and the magnitude from NKE_a. For this case, the cable provides sufficient capacitance to limit safely surges induced both electrostatically and electromagnetically.

Examination of the equations for T and M in Figure 28 shows that sloping is increased by increased values of capacitance, but that overshooting of the voltage is also increased. Thus, there is a limited range of capacitance for which machine arresters and capacitors are not required. If a calculation such as that of Figure 29 should show the need of machine arresters to limit the machine voltage, capacitors also should be used to prevent steep-front surges resulting from a sudden drop in the machine-arrester voltage when it discharges. Likewise, if the need for surge capacitors is indicated to provide adequate wave sloping (or to prevent electrostatically induced surges), machine arresters also should be used, because the capacitors usually will increase the overshooting of voltage to a dangerous value, even for the lower range of machine surge impedances. This is true unless resistance were placed in series with the capacitors. For capacitances of 0.25 microfarad or more the machine surge impedance need not be represented and the solution, for this case is given by Figure 16b. Calculations of the series resistance necessary to provide adequate damping show that it usually must exceed 50 ohms. If no other capacitance is available in the circuit for suppressing the electrostatic component, a resistance in series with the surge capacitors appreciably in excess of 50 ohms might permit this component to be severe. In general, the use of the capacitors and machine arresters is to be preferred.

Ungrounded Machines. Except for the case of an ungrounded machine connected to a wye–wye bank with both neutrals grounded, no more severe voltage conditions can be set up at the machine neutral than at its terminals. For surges produced on one or two phases of the primary winding, all bank connections, except the grounded wye–wye, produce surges of opposing polarities at the machine terminals, so that they tend to cancel at the machine neutral. For surges appearing simultaneously on all three phases of the primary winding, no appreciable voltages are transferred electromagnetically to the machine. Thus, no extra protection is required. For grounded wye–wye banks, machine arresters are always required, and, thus, capacitors should be added also. In most cases the addition of the standard capacitors provides fronts in excess of 30 or 40 microseconds, so that severe neutral voltage conditions are not likely.

Quick Method of Determining Need for Machine Protection. In the summary is given a quick method for determining the need for machine arresters and capacitors. Two steps are listed for considering electromagnetically induced surges. Step 1 pertains to adequate sloping, and the relationship is obtained from the equation for T in Figure 28. It provides a value of 12 microseconds for $0.5T$. Step 2, pertaining to adequate damping of the induced voltage, was obtained from the equation for M. It is based on the following conservative assumptions:

1. The damping is provided by only one machine.
2. Effective surge impedances ranging from 500 ohms for 2,400-volt machines to 1,000 ohms for 13,800-volt machines are assumed to be conservatively high for hydrogenerators and other slower-speed machines. A resistance of 250 to 500 ohms is assumed for turbogenerators and other machines with speeds above 1,800 rpm.
3. The voltages permitted by the station arresters are assumed to be 2.5 and 3.1 times crest line-to-line normal line voltage for 80 and 100 per cent arresters, respectively.
4. The machine insulation strength is considered to be only that given by the standard test voltages of Table II.

Magnitude of Machine-Arrester Discharge Currents

Best protection is afforded when the machine-arrester discharge currents are limited to 2,000 amperes or less, since their discharge voltages may exceed the safe protective levels for higher currents. A study, therefore, was made of this for each protection method recommended here. Using a model arrester having gap breakdown and discharge voltage characteristics scaled down by a factor of 10, the currents were measured in the equivalent circuits being studied. In Figure 30 are shown two of the solutions obtained together with the voltage characteristics of an actual machine arrester. In all cases, it was found that the currents were limited to safe values which were appreciably less than permitted by the former recommendations of 1,500 feet of line and one set of arresters for machines connected directly to overhead lines. This is illustrated in Figure 30.

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Electric-Circuit Fault-Protective Principles as Applied to D-C Aircraft Systems

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AIRCRAFT d-c electric systems are being extended constantly in responsibility and physical size. The assignment of more important tasks to the electric system emphasizes the need for the highest order of reliability of power supply, particularly in military craft exposed to unusual hazards. The increase in electric plant capacity increases the concentration of electric power which may be unleashed with damaging effect in the event of electric-circuit fault.

An electric-circuit fault results from an unintentional connection between energized conductors of different potentials or between an energized conductor and the ship structure or metallic enclosure connected thereto. The location of a particular electric fault is, of course, quite unpredictable and may appear at any point along the energized conductor system or machine internal windings. Through the by-pass circuit provided by the electric fault, excessively high current may flow which may jeopardize continued operation of the entire system, as well as constitute a serious hazard to equipment and personnel in the immediate vicinity of the fault.

It is the purpose of this paper to provide a clear understanding of system fault-protective measures as applicable to current d-c aircraft systems. Many of the principles herein described may be applied equally well to a-c aircraft systems. It will be well at this point to distinguish between equipment thermal protection and system fault protection.

Thermal Versus Fault Protection

Thermal protection aims to prevent electrical failure by interrupting the supply of power when the maximum safe operating temperature is reached. Temperature measurement may be made by embedded temperature detectors or determined from previous time–current history. The presence of thermal protection may eliminate or minimize needless burnouts of circuits or equipment. On the other hand, in aircraft service under combat conditions, there are certain functions which are extremely important, and additional combat hazards resulting from the inability of such important machines to operate may more than offset the hazards encountered by continuing the operation of these machines above maximum safe temperatures. Under these circumstances, the ability to override normal thermal protection seems quite sensible. Except during such hectic circumstances, normal properly applied thermal protection will pay dividends in the form of increased useful life. If, in the course of driving a machine above its normal thermal operating limit, a failure is induced, its usefulness abruptly ceases at that point.

In contrast to this is fault or system protection which aims to interrupt promptly the flow of power following the occurrence of electrical failure. The purposes of interruption are:

1. To minimize the energy liberated at the point of fault, thus minimizing fire hazard.
2. To avoid burning up electric conductors.
3. To minimize damage to immediately adjacent electric conductors and additional fire hazard.
4. To divorce the faulted circuit from the main electric system in order that the latter may continue to operate in a normal manner.

To permit fault protection to be overridden is most hazardous and easily may result in a destructive fire, mechanical disruption of electric-system elements in the vicinity of the fault, and/or complete collapse of the main electric system. It may be noted that the flow of 5,000 amperes into a ten-volt fault circuit represents the liberation of power in the form of heat at the rate of 50 kw. To sustain such high-current levels into a fault produces no useful result whatsoever. The overriding of fault protection is distinctly different and not to be confused with permissible reclosure using a trip-free circuit breaker which immediately will operate to interrupt the circuit if abnormally high-current flow is again established.

In many instances it may be possible to combine thermal and fault protection in a common mechanism. However, the two functions shall continue to retain their individual significance. Although moderate overcurrents may be permitted to continue for a substantial time interval before significant damage results, current flow far in excess of normal value is unquestionably the result of a fault, and no time should be lost in severing the faulted section from the system.

Obviously, it is important that any protective system correctly appraise operating conditions and function to interrupt promptly a circuit element involved in fault, yet positively refrain from interrupting in the absence of fault. Furthermore, only protective devices immediately adjacent to the fault should operate, thus limiting service area interruption to the absolute mini-

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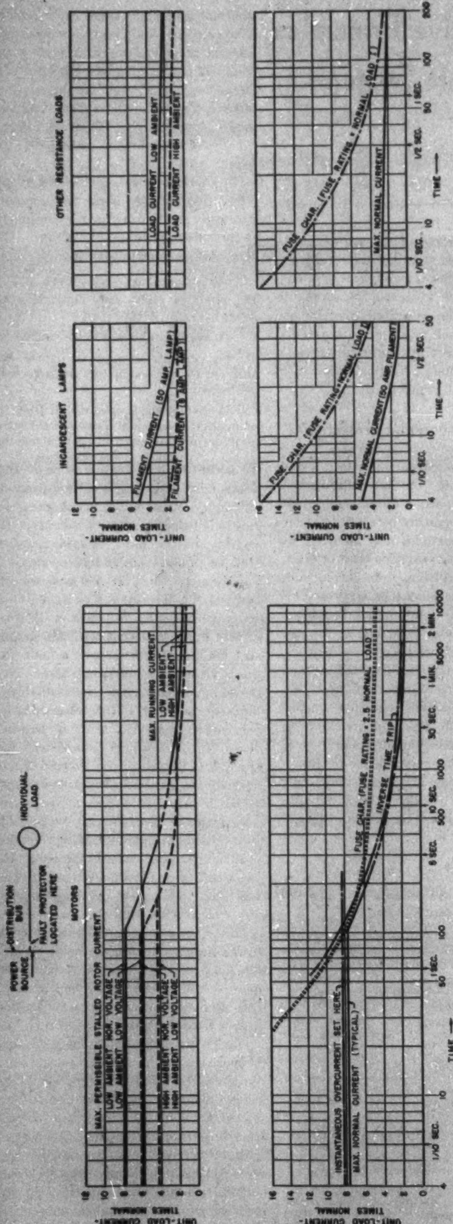


Figure 1. Individual load-current-time characteristics

Upper figures—Possible normal load currents
Lower figures—Fault-protection application

mum. Many questions immediately arise as to the manner in which this is accomplished. The first concerns the matter of positive fault detection. An open circuit is, of course, one form of fault but requires no protector action, unless and until contact with other conductors or ground occurs.

Fault Detection

The most common means of identifying fault currents, as contrasted to normal current, include:

1. Current flow in excess of the maximum normal current.
2. Current flow in a direction opposite to normal.
3. Appearance of unbalanced current in a balanced resistance double-channel circuit.
4. Appearance of current flow between the frame of a machine or electrical housing and ground.
5. A measured resistance (ratio of E/I) as viewed from the supply end of the circuit lower than that of the maximum normal load.
6. Temperature rise.

Excess Current Flow

In the majority of examples, particularly in the case of small normal ampere-rated circuits, the occurrence of an electric fault will result in a current flow considerably in excess of the maximum normal value. In some instances this may not be true. In the case of large motor circuits, for example, the normal starting current with maximum generating capacity may be greater than the fault current with minimum generating capacity. Electric faults may originate as a partial winding failure within a machine resulting in a current flow only slightly in excess of normal value, the current value becoming excessive only after substantial burning has taken place.

Reverse Current Flow

There are many electrical-equipment items (of which generator circuits and parallel feeder lines are representative) in which the flow of current in any substantial magnitude is normally in one direction only, but, as a result of electric fault in that element, substantial current flow will be observed in the direction opposite to normal.

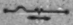
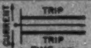
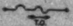
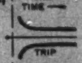
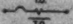
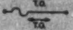
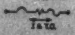
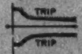
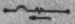
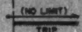
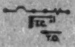
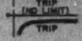
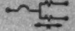
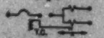
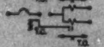
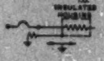
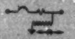
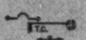
Unbalanced Current

An electric-system element containing two identical electric channels in parallel normally will have equal current flow in the same direction in both branches. The occurrence of an electric fault in one branch, however, disturbs this balance and results in a greater current flow in one leg than in the other. This difference current (one current subtracted from the other) constitutes a very reliable fault indicator. This method has the distinct advantage of being insensitive to faults occurring outside the limits of its own two paralleled branches.

Ground-Current Flow

By insulating the frame or housing, which encloses a particular system of electric winding, from ground, except for one purposely made connection through a current relay, a sensitive fault detector is obtained. An unintentional connection between internal energized windings and the enclosing housing results in current flow in the ground-detector coil circuit. Such a detector is particularly useful for obtaining sensitive fault detection on large rotating machines and for vulnerable junction boxes. Both terminals of the power winding must be insulated from the enclosing frame, of course.

Table I. Elemental Fault-Protective Devices

Type	Representative Construction		Through Boundary—Trip Boundary	Separate Trip Source Needed
Overcurrent				
Instantaneous		Circuit breaker with direct-acting series-magnet trip		No
Inverse time		Circuit breaker with direct-acting series-thermal trip		No
		Circuit breaker with direct-acting series-time-delayed-magnet trip		No
		Fuse or limiter		No
Instant and inverse time		Circuit breaker with both a series-magnet and series-thermal direct-acting trip		No
Directional Overcurrent				
Instantaneous		Circuit breaker with direct-acting polarized series-magnet trip		No
Inverse time		Circuit breaker electrically tripped from polarized inverse-time relay		Yes
Current Balance				
Instantaneous		Circuit breaker with direct-acting series-balanced-magnet trip	(No limit) Trips only on unbalanced current	No
		Circuit breaker electrically tripped from current-balance relay		Yes
Inverse time		Circuit breaker electrically tripped from inverse-time current-balance relay		Yes
Ground Current				
Instantaneous		Circuit breaker with direct-acting current trip coil connected between the insulated housing and ground	(No limit) Trips only on ground current	No
Resistance				
Instantaneous		Circuit breaker with direct-acting series-current trip opposed by shunt potential magnet	Trips only when ratio E/I becomes lower than trip coil-design value	No
Temperature		Circuit breaker electrically tripped by temperature relay	Trips only in response to temperature at relay	Yes (if used for thermal protection, line voltage may be used as trip source)

Resistance Measurement

For circuits which demand high normal currents compared with minimum available short-circuit current, a resistance-measurement detector may be useful. The fundamental principle of the detector may be obtained by assuming a balanced magnetic armature pivoted at its center. On one side of the pivot is a current coil in series with the circuit being protected which tends to move the armature in the trip direction. On the other side of the pivot is a voltage coil energized by line-to-ground voltage at the detector location which opposes the action of the current coil. At a particular level of current, operation will occur only if the voltage is below a particular critical value. At different current levels the trip point will be established at the point where the ratio of voltage to current is the same as it was in the first instance. Under conditions of electric fault, the ratio of voltage to current will be less than that obtained under normal conditions and thus would allow operation of the device.

The value of resistance-fault detection on 30-volt d-c aircraft systems is questionable under arcing-fault conditions. Fault-arc voltages may be a sizable fraction of the system operating voltage and thus result in a fault-circuit resistance substantially equal to or greater than the minimum normal resistance which would result in failure to operate.

Temperature Rise

Fault-current flow produces abnormal power loss, which is reflected in a rising temperature in conductors through which the current flows. As a matter of fact, all protectors which incorporate thermal tripping means actually operate in response to a specific temperature attained in the thermal element. Directly at the fault location the liberation of heat is accentuated and temperature rise is both rapid and high; however, unless the temperature-detecting element happens to be in close proximity to the fault location, an appreciable time elapses before heat in any substantial quantity reaches the detector location. In the case of fractional-winding faults within motors, an internal embedded temperature-responsive detector may well indicate trouble in much less time than a line-current actuated device. Note particularly that, during the existence of such marginal faults, the gross power input to the motor may be of the same order as normal rated power, although a large fraction of it may be appearing as heat at the fault location within the motor instead of being converted to mechanical power and delivered to an external load machine.

Before the several practical devices for fault detection are summarized, it will be desirable first to examine one additional topic, namely, selectivity, inasmuch as it has a very prominent influence on the make-up and application of fault-detecting devices.

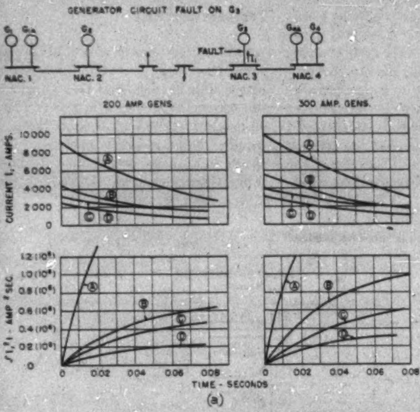


Figure 2a. Generator-circuit-fault current–time data

A. All generators and all rotating shunt-motor load on

B. All generators on

C. G_1, G_{1A}, G_2, G_3 battery on

D. G_1, G_{1A}, G_2, G_3 on

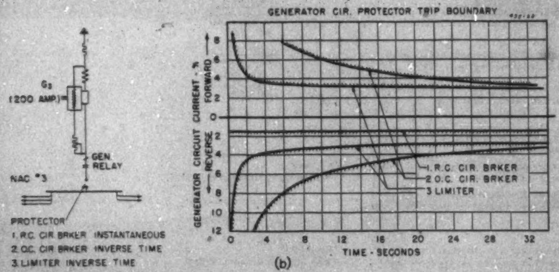


Figure 2b. Generator-circuit-protector characteristics

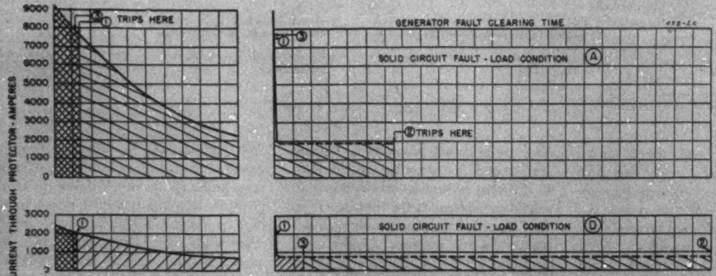


Figure 2c. Generator-circuit-fault clearing time

Selectivity

Selectivity concerns the incorporation of proper discrimination between several fault detectors which may be actuated simultaneously by a single fault in such manner that only the protectors immediately adjacent to the fault operate. The absence of complete selectivity would allow a single fault to produce service disruption over a greater area than necessary.

The complete electric system will contain a large number of individual elements or sections. The occurrence of a fault in some one section quite likely will actuate overcurrent fault detectors in a considerable number of electric channels quite removed from the fault location and may actuate reverse-current fault detectors in several sections removed from the one in fault.

To permit selective operation among the several actuated protective detectors, the element of time delay is introduced, which usually will be in the form of an inverse-time characteristic, such that the operating time diminishes as the current magnitude increases. An inverse-time characteristic is an inherent property of all fusible circuit interrupters and thermal trip devices. Time-delay characteristics may be incorporated in magnetically operated detectors in the form of an air or oil damper, or an eddy-current drag disk.

In general, the inverse-time characteristic for a particular line of equipment will be almost constant, if current is considered as a multiple of normal rating. In some cases, the time characteristic will be subject to adjustment (as in the case of air- or oil-damping mechanisms and eddy-current drag disks), and, in other cases, it will be fixed permanently by thermal design (as in the case of fuses and ordinary thermal trips).

A summary of the various common fault detectors with and without time-delay features is presented in Table I.

In attempting to evaluate the selectivity which will be obtained in a particular system among several simultaneously actuated fault detectors, it becomes necessary to examine the operating tolerance in a particular protective device.

Operating Tolerance of Fault Protectors

The operating tolerance of protective device is quite distinct from its ability to withstand normal service conditions, such as fault-current magnitudes, vibration, and temperature. Variations in its operating current–time characteristic may result from one or more of the following:

1. Manufacturing tolerance.
2. Variation in ambient temperature.
3. Repetitive operation in service.
4. Degree of electric load carried prior to fault occurrence.
5. Variation in altitude.
6. Total clearing time as contrasted with operating time.

Manufacturing Tolerance

To construct supposedly duplicate devices which individually will exhibit identical characteristics is virtually impossible. The degree to which individual items deviate from the mean may be narrowed by more refined construction and more accurate control of manufacture, which is reflected in increased man-hours per device produced. Reasonable manufacturing tolerance thus constitutes a compromise between performance accuracy and man-hours invested in its construction. A reasonable permissible manufacturing tolerance might be plus or minus five per cent variation in the minimum current, which would cause operation, and plus or minus 10–25 per cent variation in time to operate at ten times normal current.

Ambient-Temperature Variation

All devices which operate on a thermal principle will be influenced by ambient-temperature variations to a degree depending on the operating temperature of the thermal element. Both the minimum operating current and the time at elevated current will be affected. The ambient temperature will directly establish the required temperature rise to reach the tripping point. The minimum operating current will be affected substantially as the square root of this required temperature rise, inasmuch as generated heat is proportional to current squared. The operating time at a high-current value—say ten times normal—will be substantially proportional to the required temperature rise. Table II indicates the order of variation in operating current and operating time which would be expected for:

1. A thermal trip operating at 120 degrees centigrade.
2. A zinc fusible unit operating at 420 degrees centigrade.
3. A copper fusible unit operating at 1,080 degrees centigrade.

The current required to operate magnetic types of relays is insensitive to ambient temperatures within the aircraft operating range, although associated time-delay devices may or may not be affected, depending on their type.

Repetitive Operation in Service

Operating characteristics may be altered by repeated use in service, because of such things as mechanical displacements resulting from wear and changes in the magnetic strength of polarizing permanent magnets. Of a distinctly different character is the matter of interference with free motion of working parts due to dust or dirt accumulation which can be corrected by proper maintenance. Tolerances of this character can be covered partially by equipment manufacturing specifications, and the balance must depend on adequate maintenance procedures in the field.

Table II. Ambient-Temperature Variation

From 25 Degrees Centigrade

Ambient Temperature (Degrees Centigrade)	Thermal Character (Degrees Centigrade)	Required Rise to Trip (Degrees Centigrade)	Minimum Operating I	Time at 10 I_N
25	Bimetal (120)	95	1.0	1.0
0	Bimetal (120)	120	1.12	1.26
40	Bimetal (120)	80	0.92	0.84
25	Zinc fuse (420)	395	1.0	1.0
0	Zinc fuse (420)	420	1.03	1.06
40	Zinc fuse (420)	380	0.98	0.96
25	Copper fuse (1,080)	1,055	1.0	1.0
0	Copper fuse (1,080)	1,080	1.01	1.02
40	Copper fuse (1,080)	1,040	0.99	0.985

Previous Load-Current History

All thermally operated devices will assume a temperature on the thermal element above the ambient temperature as a result of normal load-current flow. A subsequent application of high-fault-current flow will entail a shorter operating time, since the thermal element already has been elevated somewhat in temperature. Of course, the minimum operating current level will be unaffected by previous load-current flow.

Altitude Variation

The minimum current which will operate any thermal type of overcurrent element is established by the balance between heat generated and heat dissipated from the thermal element. As altitude is increased, air density becomes less, and the heat dissipated from the thermal element at a given temperature also diminishes. Thus, there will result a tendency to operate at a lower minimum current level at increased altitude, unless compensated by an appropriate reduction in ambient temperature. Under high overcurrent conditions, on the other hand, the tripping point is controlled mainly by heat stored within the thermal element itself, and, hence, altitude would have substantially no effect on the operating time at high current.

Total Clearing Time Versus Operating Time

All circuit-protective devices to a greater or lesser extent allow current flow to continue beyond the operating or trip point. This additional current flow passes through other protective devices and tends to propel them to the operating point. At moderate overcurrents the current which flows after a particular protective device has been tripped is not a substantial part of the total. At high currents, on the other hand, the heating effect of the current which flows after the device has been tripped may become comparable with that required to operate. In the latter case, one overcurrent device which requires twice as long a time to operate than one with which it is in series would tend to be operated as a result of the current flow through the first unit in the course of its operation. At high altitudes the tendency for current flow following operation of most protectors is increased.

To summarize, it is necessary to recognize that the operating characteristic of a particular protector constitutes a band rather than a line on a time–current curve. Furthermore, current flow which continues for a short interval after the operation of one protector tends to operate an adjacent protector through which this same current is flowing, although the two protectors were intended to be selective. Protectors in different locations of the ship may be affected differently in respect to ambient temperature, atmospheric pressure, previous load-current level, and so forth. The longest possible total conduction time of a protector nearest the fault location must be shorter than the shortest possible operating time of the next removed protector between which selective operation is to be attained.

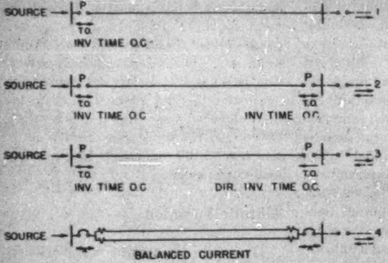
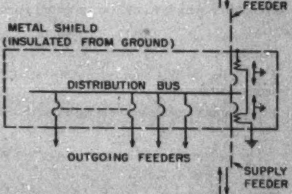


Figure 3a (left). Feeder-system tie-circuit elements

Figure 3b (below). Selective bus protection using ground-current flow for detection



In the absence of accurate band-width characteristics, it would seem reasonable to require a time discrimination ratio of 3:1 at high-current levels (above ten times normal) and 2:1 at moderate overcurrents (below six times normal) to be considered as being selective.

In approaching the matter of fault-protection application procedure, there are two subjects which promptly will assume a significant position, namely, the character of normal load currents and the incorporation of duplication of service.

Normal Load-Current Character

The normal load-current requirements of the various individual load circuits differ, depending on their nature. Representative of the several broad types are:

1. Rotating machines.
2. Incandescent lamps.
3. Static resistance loads.

1. Rotating machines, including series and shunt motors, dynamotors, and inverters, are unique in that the normal load current is controlled by the magnitude of externally applied load rather than by the internal-winding resistance (except at start). With the rotating-machine rotor at standstill, the current demand is primarily controlled by the internal resistance of the windings but is influenced by the magnitude of impressed voltage and machine-winding temperature. This is illustrated in Figure 1. If the rotor is prevented from turning, there will exist a specific time for each condition of impressed voltage and machine temperature during which current may be permitted to flow before the internal windings reach an injurious temperature, such as shown in Figure 1.

Under normal conditions, with the rotor free to turn, the rotor will accelerate rapidly, and the current demand will drop simultaneously approaching the steady running condition. While the rotor runs continuously at constant load, there will be a specific current level above which injurious winding temperatures will be realized, the limiting current level of which will be higher in the presence of reduced ambient temperatures and/or decreased altitude. For short time intervals a higher running current will be permissible, the magnitude of which will depend on the winding temperature already reached as the result of prior loading.

2. Conventional tungsten-filament incandescent lamps, when initially energized, demand a high inrush current which rapidly decays as the filament comes up to temperature. In contrast to motor current, the current decay is controlled by the lamp-filament thermal design and is quite independent of external effect. The time constant of current decay is quite short (in the order of $^{1}/_{35}$ second), although the time constant is influenced by filament diameter, becoming longer with increasing diameter. It is generally true that fuses, the current rating of which is equal to or in excess of the lamp normal rating, will not be operated by the heating inrush. It might be well to review this characteristic from the standpoint of aircraft service, since lamp-filament diameters may be greater and fuse melting time faster than that on which previous experience is based.

3. Other resistance loads characterized by heater circuits, magnetic operating coils, and so forth, tend to demand a fixed current commensurate with normal rated current immediately on being switched across the line. The resistance of the load may be influenced somewhat by ambient temperature; however, the variation in current demand resulting from this is much more moderate than that encountered in the other load classes.

Service Reliability—Duplication of Service

A consideration of protective-equipment application to aircraft systems will involve almost immediately the question of service reliability of duplication of service in view of the hazards to which the electric system is subjected. By service reliability is meant the ability to maintain electric power supply to a particular point of load application with any specific element or section of the supply system out of service. Furthermore, it shall be understood that, should such element or section become faulty while in operation, it shall sever itself automatically from the system without disrupting service over other channels. To be effective to full extent, the complementary power-supply channels shall extend to and include independent generating or source units.

Fault-Protection Application

In the treatment of fault-protection application, the complete system may be subdivided conveniently into classes or zones in which the problems are similar. The several classes of circuit equipment are identified as follows:

1. Individual load circuits.
2. Individual generator circuits.
3. Main-feeder system.
    A. Lines.
    B. Busses or junctions.

Individual Load-Circuit Protection

All individual load circuits represent radial extremities of the electric system. Excessive current flow in these branches is immediately indicative of a fault in that particular element. Instantaneous overcurrent trip set slightly above the maximum normal circuit current is very useful in quick dispense of the faulty section. It is well to bear in mind that fuse protection applied to the smaller ampere-rated circuits provides fault protection, which is substantially instantaneous in view of the fact that fault-current magnitudes may be very high in comparison with normal circuit rating (100 times or more). Fuses may be considered as providing instantaneous protection in all cases where fault-current magnitudes are in excess of 30 times the fuse normal rating.

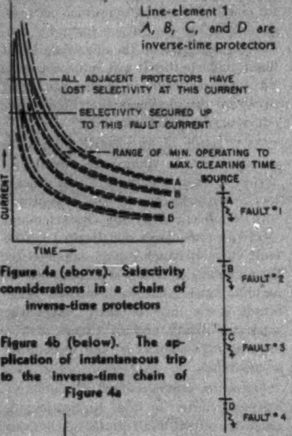

Line-element 1
A, B, C, and *D* are inverse-time protectors
ALL ADJACENT PROTECTORS HAVE LOST SELECTIVITY AT THIS CURRENT
SELECTIVITY SECURED UP TO THIS FAULT CURRENT
RANGE OF MIN. OPERATING TO MAX. CLEARING TIME
SOURCE
A
B
C
D
FAULT #1
FAULT #2
FAULT #3
FAULT #4
CURRENT
TIME

Figure 4a (above). Selectivity considerations in a chain of inverse-time protectors

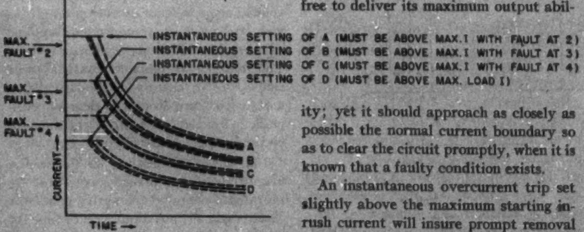

MAX. FAULT #2
MAX. FAULT #3
MAX. FAULT #4
INSTANTANEOUS SETTING OF A (MUST BE ABOVE MAX. I WITH FAULT AT 2)
INSTANTANEOUS SETTING OF B (MUST BE ABOVE MAX. I WITH FAULT AT 3)
INSTANTANEOUS SETTING OF C (MUST BE ABOVE MAX. I WITH FAULT AT 4)
INSTANTANEOUS SETTING OF D (MUST BE ABOVE MAX. LOAD I)
A
B
C
D
CURRENT
TIME

Figure 4b (below). The application of instantaneous trip to the inverse-time chain of Figure 4a

Motor Circuits

Characteristic normal current requirements of motor circuits have been established previously and are illustrated in the upper part of Figure 1. The maximum starting inrush current for a particular motor is influenced by ambient temperature and the system-voltage dips during starting. The maximum time which the stalled-rotor current could be permitted to flow without burning up the windings is influenced by:

1. The design characteristics of the machine.
2. The ambient temperature in which it is being operated.
3. The available voltage which is impressed on the motor windings.

With the motor running, a time limit will be imposed for currents in excess of the normal continuous rating, the permissible time becoming shorter as the current magnitude becomes greater.

The maximum boundary of motor normal current recognizing the variation in the several influencing factors is shown in the lower part of Figure 1. In the application of system or fault protection to the motor line circuit, it is important that the trip boundary does not enter the zone of normal permissible operating current in order that the motor may be free to deliver its maximum output ability; yet it should approach as closely as possible the normal current boundary so as to clear the circuit promptly, when it is known that a faulty condition exists.

An instantaneous overcurrent trip set slightly above the maximum starting inrush current will insure prompt removal in the event of a motor internal-winding fault of serious character or a motor-line-circuit fault. Additional protection could be obtained by including also an inverse-time overcurrent trip closely approaching the maximum permissible current–time curve of the motor. It is not to be inferred that this characteristic would provide thermal protection to the motor, but rather that it would function to remove the motor as soon as it definitely were damaged or unserviceable. If the motor includes thermal protection, as it normally should, the latter would have tripped off the motor in a shorter time. The disadvantage of attempting to use an inverse-time protective characteristic alone is clearly evident on the time–current chart. In that case, currents far in excess of normal maximum currents would be permitted to flow for a substantial time interval. For the case illustrated, a current of 16 times the motor normal rating would be permitted to flow for about one-half second, which not only would impose a severe burden on the power-supply system but also would constitute an accentuated fire hazard at the fault.

It should be realized that a line-overcurrent protector cannot provide infallible fault protection. Imagine, for instance, that a motor develops a fault in one coil while otherwise running normally. Initially, this single-coil fault would result only in a moderate increase in line current, probably not exceeding minimum starting inrush current. A substantial time interval may elapse before the burning has progressed to a point where the line protective equipment will operate. During this interval substantial amounts of energy may be liberated at the point of fault which are conductive to increase fire hazard. This would be particularly true in the larger-size machines, since a greater amount of energy could be liberated at a particular fault location before circuit tripping occurs.

A much-improved type of protection for faults of this nature can be obtained by the application of ground-current detection; but this requires that the motor structure be insulated from the ship frame and all internal windings be kept insulated from the ship frame and the motor structure, with the ground connection made external to the motor frame.

To illustrate the limitation in protection afforded by fuses in the case of motor circuits, a representative fuse operating characteristic has been plotted on the current–time chart. To avoid tripping during the maximum permissible time of stalled-rotor current flow, the fuse must have a normal current rating several times the motor normal continuous current rating. The fuse operating characteristic thus provides an excessively long time to operate in the presence of moderate overcurrent, up to four or five times the motor normal rating, and, at the same time, it requires a substantial time interval for operation in the presence of currents considerably in excess of the maximum stalled-rotor current. Such a characteristic represents merely a compromise protection, both at very high currents and at moderate overcurrents.

Incandescent-Lamp Circuit

The inverse-time–overcurrent characteristic represented by the conventional fuse is well-adapted to the protection of incandescent lamp circuits. While the initial current surge into a cold lamp filament tends to approach about 15 times normal, the duration is quite short. It may be noted that the inrush current on a 50-ampere filament has dropped to about five times normal in $^1/_{15}$ of a second. The inrush surge current for smaller-ampere-rated lamps decays more rapidly. The selection of fuses with current rating substantially equal to the normal lamp current will provide good protective characteristics, yet be free of false operation on initial heating inrush for lamp-filament current ratings up to 50 amperes.

Other Resistance Loads

Other resistance loads would include heaters of all types, solenoid operating coils, machine field circuits, and so forth. For the major part, individual circuits of this character are of low normal ampere rating, few of which would be in excess of 30 amperes. The protective characteristic represented by the conventional fuse is well-adapted to this class of circuit, since, as a result of small normal ampere rating, the operation becomes substantially instantaneous in the presence of fault currents of 900 amperes or more. In selection of fuse normal ampere rating, it should be recognized that many such load circuits consist of copper windings or are composed of circuit material exhibiting considerable temperature coefficient of resistance, which will mean that the normal current demand at low ambient temperatures will be increased substantially. Therefore, it usually will be necessary that the fuse-rated currents be selected on the basis of $1^1/_2$ to 2 times the normal load-circuit current at a 25-degree-centigrade ambient temperature.

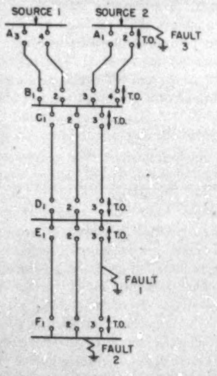


Figure 5a. Selectivity considerations in a multiple paralleled-line system using only inverse-time protectors

Line-protective element 2 used throughout. With fault at:

1, F_3 should clear before F_1 F_2 or E_1 E_2 operate

1, current through E_3 and F_3 should not operate C_1 C_2 (C_3 D_3 out of service)

1, current through E_3 and F_3 should not operate A_1 A_2 (source 1 out of service)

2, E_1 E_2 E_3 should clear before C_1 C_2 operates (C_3 D_3 out of service)

2, E_1 E_2 E_3 should clear before A_1 A_2 operates (source 1 out of service)

3, A_1 A_2 should clear before A_3 A_4 (not possible without directional or bus protection)

Individual Generator-Circuit Protection

Generators constitute the prime source of electric power and are of the utmost importance to the operation of the electric system. They are deserving of the most careful consideration in the application of protective equipment to insure that their operation will not be interrupted falsely.

It is considered preferable that generator loading should be subject to manual supervision, and that automatic thermal protection should be omitted. (A warning signal actuated by generator overtemperature is, of course, acceptable.) Should a generator be cut out automatically, as a result of forward overcurrent, it would result merely in further increase of load levels on remaining generators. To permit reasonable supervision by the responsible member of the flight crew, it would be recommended that individual generator ammeters be provided (miniature style with coarse scale). With this aid any erratic operation of a particular generator would be brought to the supervisor's attention immediately and, by its nature, allow him best to appraise the situation and decide whether this generator could be restored to normal performance, whether its output must be considered negligible for the duration of that flight, and whether it will be necessary to ask for a reduction in nonessential load to permit completion of that flight without danger to generator units.

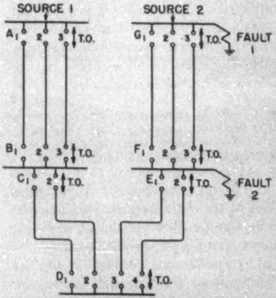


Figure 5b. Selectivity considerations in a multiple paralleled-line loop system using only inverse-time protectors

Line-protective element 2 used throughout. Section internal-fault selectivity similar. With fault at:

1, G_1 G_2 G_3 should clear before A_1 A_2 A_3 operate

2, E_1 E_2 should clear before C_1 C_2 operate

(Neither of these can be assured without other types of protection or reliable fault-current sources on load busses)

As a result of an electric fault in a generator or its immediate circuit, an abrupt reversal of current in that generator circuit will occur; the magnitude will be influenced by the character and location of the fault and limited to the fault current, which can be supplied by the remaining electric system in operation at that time. The fault-current magnitude easily may be many times the generator rating and in excess of the interrupting ability of the generator-circuit relay.

Figure 2a portrays system characteristics pertinent to the application of generator-circuit fault protection. Four conditions of system operation (*A*, *B*, *C*, and *D*) have been recognized as applied to a representative six-generator system. The battery equipment consists of a single-type G_1 battery in good condition at a temperature around 25 degrees centigrade. An electric fault is assumed to occur on G_3 generator circuit.

The curves of Figure 2a define the magnitude of I_1 (current contributed by the main system to the faulty generator circuit) and ΣI_1^2t as a function of time for all four operating conditions and for both 200- and 300-ampere generators in the presence of a low-resistance generator-circuit fault. The ΣI_1^2t is quite useful in judging the operating time of thermally operated protectors, such as thermal trip circuit breakers and fusible links.

Should the fault be of arcing character or contain substantial circuit resistance, the initial current magnitude and the subsequent decay would be diminished. If sufficient fault resistance is present to diminish the initial current to half the solid fault value, subsequent current magnitude would be substantially constant and actually might increase with time.

Figure 2b identifies the proper location of the generator-circuit protector and defines the current–time operating characteristics of three types of protectors (based specifically on a 200-ampere-generator rating):

1. Instantaneous reverse-current circuit breaker.
2. Inverse - time - overcurrent circuit breaker.
3. Limiter (fusible link protector).

Figure 2c identifies the fault clearing time for the three protector types, based on a low-resistance fault and 200-ampere-rated generators, for the following conditions of operation:

1. Operating-condition *A*—all generators and rotating load in service.
2. Operating-condition *D*—generators G_1, G_{1A}, G_2, and G_3 alone in service.
3. Engine 3 dead and associated generator relay contacts frozen closed—no other electric fault present.

The predicted clearing time for these specific cases is given in Table III.

Although this specific comparison has been based on the use of 200-ampere generators, it will be evident that performance with generators and associated protectors of other ratings will be similar.

Prompt disposition of fault-current flow in aircraft d-c systems is important. In the case of a low-resistance fault, the system voltage is reduced to extremely low values, which, if sustained for an interval of more than about $^1/_{30}$ second, will allow generator-relay contactors and magnetic switches throughout the system to lose their holding force which is conductive to false dropout and welding of contacts. With the system electric load composed of a substantial amount of rotating shunt or compound-wound motor equipment, a serious depression of system voltage for more than one-half second may be conductive to electric stalling from which recovery may be effected only when a portion of load equipment is disconnected manually. Any unnecessary prolongation of fault-current flow, of course, means increased fire hazard.

The reverse-current circuit breaker, being insensitive to forward current flow, can be made instantaneous in action and set to trip at a current just above the maximum reverse current incident to normal operation. Extensive tests disclose that this current, with most rapid engine deceleration, will not exceed about 75 per cent of generator rating. A circuit-breaker trip setting of about 150 per cent is judged appropriate to insure freedom from false tripping.

Overcurrent protectors necessarily must incorporate time delay to avoid tripping on momentary starting-current demand and short circuits occurring on the system remote from the particular generator circuit in question. As the overcurrent protector is nondirectional, the same time delay will be incurred in clearing fault currents fed to a faulty generator circuit.

Generator-Field-Circuit Interruption

Upon the occurrence of a fault in a generator circuit, the local protector functions to divorce that unit from the balance of the system. The generator, however, continues to rotate at normal speed, and the voltage regulator attempts to recover normal terminal voltage by cutting out resistance in the generator field circuit. The individual generator in trouble is thus free to continue to supply fault current locally, which would result in objectionable sustained sputtering at the point of fault and might easily result in sufficient generator current to cause a burnout of that unit or even start a fire. To correct this deficiency, the operation of the generator-circuit protector also should open the generator field circuit. This function can be incorporated readily in a circuit breaker by the inclusion of an auxiliary switch connected in the generator field circuit to open the latter when the mechanism moves to the trip position.

Generator-Circuit-Protector Location

The generator-circuit fault protector should be located directly at the junction of the individual generator circuit with the main distribution system, so as to include within the protected zone the generator line circuits and associated auxiliaries, such as generator relay and voltage regulator. (In similar manner, the individual load-circuit protectors should be located directly at the junction between such individual circuits and the main distribution bus system.)

Main-Feeder-Line Protection

The electric distribution system will incorporate a multiplicity of main feeder-line sections which interconnect the power source busses with the several utilization busses.

Appropriate fault-protective equipment applicable to elemental line sections is portrayed in Figure 3a. Element 1 incorporates an inverse-time–overcurrent protector at the end nearest the source and limited in application to conditions in which the line element is subject to one-way feed only. Element 2 differs from element 1 in that inverse-time–overcurrent protection is applied at both terminals of the line and is suitable for applications which energize the line from both ends. Element 3 contains inverse-time–overcurrent protection at the end nearest the source and directional inverse-time–overcurrent at the terminal farthest removed from the source, and is likewise applicable to double-ended feed. Element 4 incorporates balanced-current protection at both ends of the line section.

The application of these several elemental line sections to form composite systems will be taken up in sequence later, and their relative merits will be evaluated.

Bus Protection

It will become evident shortly, that bus sections at which a number of line elements converge to a common junction constitute vulnerable spots which are difficult to protect satisfactorily. An effective sensitive form of bus protection can be secured in the manner illustrated in Figure 3b. The metallic structure which supports the bus work, together with the metallic enclosing shield or guard members, is attached to the ship structure through electrical insulating supports. This insulated framework is then connected to the ship frame through an electric circuit containing a trip coil on a circuit protector of each supply feeder which forms a junction at that point. Any electric connection between energized electric conductors and the bus-supporting members or guard shield immediately circulates current through the several trip coils, which promptly interrupt all sources of supply. Individual load circuits which might radiate from this same bus would be equipped with protective devices at the junction to the bus.

Selectivity by Inverse-Time–Overcurrent Protection

Figure 4a illustrates the application of inverse-time characteristics to obtain selectivity. Consider a chain of feeder-line sections connecting several utilization busses with the source bus. *A*, *B*, *C*, and *D* represent inverse-time–overcurrent protectors, each associated with its own elemental line section. By appropriate selection of the inverse-time–current characteristic of the protectors at *A*, *B*, *C*, and *D*, as illustrated in the left-hand figure, selectivity of fault clearing is secured for moderate overcurrents. For a fault at location 4, circuit-protector *D* functions to clear the fault current in less time than that required to operate either *C*, *B*, or *A*. Likewise, a fault at location 3 causes protector *C* to clear the circuit in less time than that required to operate *B* or *A*. Similarly, a fault at location 2 causes protector *B* to clear the circuit in less time than required to operate *A*. Thus, complete selectivity would be attained.

It will be observed that, as the magnitude of fault current increases, the clearing time of the faster protector approaches more closely the minimum operating time of the next slower protector, and they overlap ultimately. At this current, selectivity between those adjacent protectors is lost; that is, for a fault at location 4, protector *C* may operate before protector *D* has cleared the fault circuit, and thus both *C* and *D* open. At high overcurrents a four-section chain, such as illustrated here, which possessed perfect selectivity at moderate overcurrent, may lose selectivity completely; that is, a single fault at location 4 may cause all four protectors, *A*, *B*, *C*, and *D*, to open.

It is important to test the selectivity of nverse-time cascaded protectors at the highest level of fault currents to which they will be subjected. If selective at these levels, there will be no doubt of their selectivity at lower-current levels.

Addition of Instantaneous Trip

The addition of instantaneous trip to inverse-time–overcurrent protectors may prove useful, under certain circumstances, in extending the current range over which selectivity will be obtained and provide faster clearing time at higher overcurrents (Figure 4b). The utility of instantaneous trip depends on the existence of substantial impedance in the various line sections, such that there is a distinct difference in the fault-current level for a fault at locations 1, 2, 3, and 4, respectively. The reason for this will be evident. The instantaneous trip setting applied to protector *C* must be above the maximum fault current ever encountered for a fault at location 4. Otherwise, the latter fault would cause protector *C* to operate in error. Unless the fault-current level at 3 is substantially greater, an instantaneous trip applied to protector *C* would be useless, since there would be no occasion for it to operate.

It is doubtful, because of the small change in fault-current magnitude as the fault location moves along the line section, whether the addition of instantaneous trip to inverse-time protectors will be of general utility in main-feeder-circuit lines on aircraft installations. Instantaneous trip will be useful in the protection of individual load circuits and other circuit extremities, since the appearance of overcurrent in these branches is directly indicative of faults and should signal a trip operation on the protector.

Selectivity in Multiple Paralleled Lines Using Inverse-Time–Overcurrent Protection

One purpose of multiple paralleled lines is to provide service reliability. Power supply to the several utilization busses can be continued in the absence of one elemental line section of any group of paralleled conductors. In order that any particular elemental line section be capable of clearing at both ends without resulting in automatic tripping at any other point involves considerations of selectivity. The problem of examining any particular system configuration to insure proper selectivity under all operating contingencies becomes quite tedious and laborious.

Figure 6 illustrates the considerations involved in examining the selectivity of such a system. Figure 5a shows a radial or stub feed, while Figure 5b portrays a loop feed.

Application of Directional Tripping

It may be noted that certain instances exist in which inverse-time–overcurrent protectors are simply incapable of insuring selective operation. In Figure 5a, a fault at location 3 equally well may cause operation of protectors A_3 and A_4, or B_1 and B_2, as operation of the correct protectors A_1 and A_2, or B_3 and B_4. The substitution of elemental-line 3 (Figure 3a), incorporating directional overcurrent at the end remote from the source for all the *A*–*B* channels, would allow this deficiency to be corrected. Similar deficiencies will be observed in the loop-feed arrangement for faults on either source bus or on either of the intermediate load busses.

It was observed that directional tripping could be used to advantage in overcoming deficiencies in multiple paralleled line systems. Selective clearing of one line of a double line circuit may be accomplished in similar manner using elemental-line 3 as outlined in Figure 6a.

The single-channel loop circuit, as portrayed in Figure 6b is another typical example making use of directional tripping to secure selectivity.

It must be conceded that directional inverse-time tripping characteristics, while they do permit the attainment of assured selectivity under some difficult circumstances, do not materially simplify the selectivity problem and still require careful consideration of time factors among numerous protectors which may be actuated simultaneously. All systems incorporating a multiplicity of interconnected electric channels employing overcurrent fault detection will necessarily entail considerable complexity in regard to the attainment of correct selectivity.

Duplicate Nonparalleled Lines

The use of nonparalleled lines for service reliability allows a substantial simplification of the selectivity problem when employing overcurrent fault detection. The fundamentals of such an arrangement are illustrated in Figure 7a. Each feeder system represents a radial or stub-feed channel in which each protector shall be time selective with the next adjacent protector. A fault on one such channel produces no substantial fault-current flow in other channels. Hence, the faults on one channel inherently have no tendency to open the protectors on the complementary feeder channels. Individual load distribution centers are each provided with transfer switching, either manual or automatic, which allows that particular load to be derived, from either of the two complementary feeder channels. Under normal operating conditions, the transfer switches would be positioned so as to divide the total load between the two feeder channels, thus minimizing system voltage drop, power losses, and conductor heating.

Another important advantage of this system lies in the ease in which field test-

Table III

	Protector 1 (Second)	Protector 2 (Seconds)	Protector 3 (Seconds)
Operating condition *A*	0.013	6.5*	0.018
Operating condition *D*	0.018	24.0**	1.5
Engine dead, relay frozen	0.011	22.0	2.5

* If it is assumed that 1,800 amperes are sustained through protector.

** If it is assumed that 750 amperes are sustained through protector.

ing may be applied to insure that all sections are initially intact. For example, should the right-hand-protector *B* be operated or the line section interconnecting *B* with *C* interrupted, this will be evident immediately in the absence of voltage on the right-hand side of the center and lower load transfer switches.

Balanced-Current Line and Ground-Current Bus Protection

The real answer to the selectivity problem in a multiple-channel interconnected system is obtained through the use of fault detectors which are sensitive only to internal faults occurring within the associated system element. Such a system is illustrated in Figure 7b.

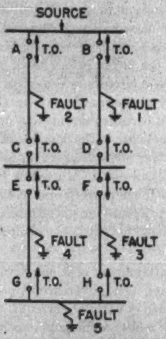


Figure 6a. Selectivity considerations in a parallel-line system using directional tripping

Line element 3. With:
Fault at 1, *D* must clear before *C* operates
Fault at 2, *C* must clear before *D* operates
Fault at 3, *H* must clear before *G* operates
Fault at 4, *G* must clear before *H* operates
Apply directional tripping using line-protective element 3. Other considerations:
Fault at 3 combined current through *F* and *H* shall not operate *A* alone
Fault at 5 combined current through *E* and *F* shall not operate *A* alone
Fault at 1 (line *EG* open) *H* shall not operate as a result of rotating machine back feed from lower bus

Each elemental line section is of the balanced-current type (line-protective-element 4). A fault on either conductor of any such line section results in unbalanced current, between the two lines, which operates the protectors at each end of that line section. Through fault currents in other branches of the system remain balanced and produce no tendency to trip the protector. (Obviously, small error or unbalanced current may result because of differences in contact resistances in the two parallel branches, for which allowance would be made in selecting the minimum operating current unbalance.)

Fault protection, using differential ground-current fault detection as outlined in Figure 3b, is provided for individual junction busses. The occurrence of a bus fault created by a dislocated bolt or nut, a screw driver or wrench, or any other cause, will result in immediate tripping of all sources of supply to that local bus.

Obviously, individual load and generator circuits should contain fault-protective equipment located directly at the point of connection with one of the junction busses.

The outstanding advantages of this type of system arrangement are:

1. Instantaneous fault-interrupter action is obtained in response to system faults at all locations. Time is not a factor in securing selective tripping.

2. The sensitivity of fault protection is limited only by the inherent error current in circuit elements. There will be no tendency to operate on large swings in normal load current or as a result of high starting-current demands.

3. Fault selectivity is an inherent property of individual system elements eliminating tedious time-consuming checking which would be required with overcurrent fault detectors.

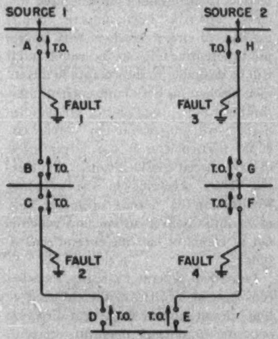


Figure 6b. Selectivity considerations in a closed loop system using directional tripping

Line element 3. With:
Fault at 1, *B* must clear before *C* operates
Fault at 2, *C* must clear before *B* operates
Fault at 2, *D* must clear before *E* operates
Fault at 4, *E* must clear before *D* operates
Fault at 4, *F* must clear before *G* operates
Fault at 3, *G* must clear before *F* operates
Apply directional tripping using line-protective element 3
Fault at 1, *B* must clear before *D* operates
Fault at 2, *D* must clear before *F* operates
Fault at 2, *C* must clear before *A* operates
Fault at 4, *F* must clear before *H* operates
Fault at 4, *E* must clear before *C* operates
Fault at 3, *G* must clear before *E* operates
This forms two four-section inverse-time-selective chains, one including *A*, *C*, *E*, and *G*, the other including *H*, *F*, *D*, and *B*

Summary of System Fault Protection

While system electric faults in 30-volt d-c aircraft systems eventually, in the majority of cases, will burn themselves clear, they cannot be considered to do so dependably. It has been demonstrated by test that electric faults in the larger-size conductors may weld solid to the ship framework and thereafter conduct system short-circuit current almost indefinitely. In the process of burning clear, substantial amounts of molten metal may be thrown for considerable distances constituting a distinct fire hazard. Furthermore, if power conductors are broken and not rigidly supported close to the arcing portion, the unsupported cable end may follow up and produce persistent arcing and sputtering for a substantial time interval. Not only is fire hazard increased, but also, the persistent arcing fault represents the equivalent of a heavy electric load imposed on the power supply system which may allow the generating system to become stalled and capable of recovery only after a substantial portion of rotating machine load has been disconnected.

In the light of these circumstances, it is considered essential that an effective system of automatic fault interruption be employed.

Principles of Automatic Fault Interruption

System fault-protective equipment should allow the ultimate ability of load equipment to be utilized with complete freedom from false tripping of any protectors throughout the range of operating conditions in respect to temperature, altitude, vibration, and so forth. It is important that all circuit protectors possess assured ability to interrupt the maximum possible fault current to which they can be subjected, such ability having been demonstrated by test.

Upon the occurrence of an electric fault, fast operating speed of the immediately associated circuit protectors is extremely important. Solid faults should be disposed of within a time interval of about $^1/_{30}$ second, and severe arcing faults (equivalent to an extremely large starting-current demand) may result in elec-

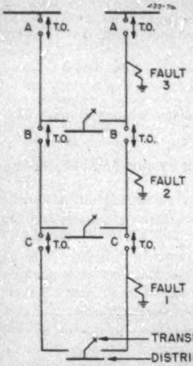


Figure 7a. Selectivity considerations in a duplicate-circuit nonparalleled system using only inverse-time protectors

Line-protective element 1 used throughout. With fault at:

1, *C* shall clear before *B* operates

2, *B* shall clear before *A* operates

3, *A* shall clear as fast as possible, yet satisfy fault at 2. Normally half of transfer switches on one line and half on other

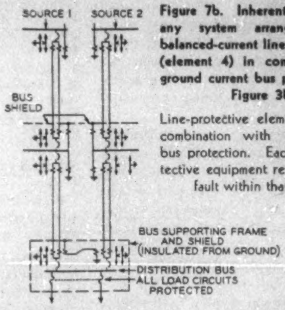

SOURCE 1
SOURCE 2
BUS SHIELD
BUS SUPPORTING FRAME AND SHIELD (INSULATED FROM GROUND)
DISTRIBUTION BUS
ALL LOAD CIRCUITS PROTECTED

Figure 7b. Inherent selectivity in any system arrangement using balanced-current line protection (element 4) in combination with ground current bus protection (see Figure 3b)

Line-protective element 4 used in combination with ground-current bus protection. Each section protective equipment responds only to fault within that section

tric-system collapse if maintained for more than approximately one-half second.

The importance of simplicity cannot be overemphasized and applies equally well to equipment design, application procedure, operation, and field-servicing practices. In general, a simple type of system design employing simple protective devices, for which performance characteristics can be defined easily and accurately, is to be preferred to a more complex system requiring much laborious analysis to verify operating characteristics.

Fault-Protection Application

Fault-protective equipment application may be divided into three groups:

1. Individual load-circuit protection.
2. Individual generator-circuit protection.
3. Main-feeder-line and junction-bus protection.

Individual load circuits constitute extremities of the electric system in which the appearance of excess overcurrent is immediately indicative of fault. Fault-current flow is in the same direction as normal operating current. Instantaneous overcurrent protection set above the maximum normal operating current provides effective high-current fault protection for moderate circuit-current ratings. Fuses may be considered as essentially instantaneous at fault-current levels of 30 times or more their normal current rating, and thus provide a high order of fault protection to the smaller-ampere-rated circuits. For the larger-ampere circuits mechanical circuit breakers with appropriate actuating trips are indicated, and it will be desirable to incorporate in a common mechanism both fault-current interrupting ability and ability to perform normal switching operations. Motor circuits, particularly in the larger sizes, deserve special consideration in that electric faults occurring in the armature winding during normal operation may result initially in a current flow which is not in excess of starting-current magnitudes.

Individual generator circuits, likewise, constitute extremities of the electric system. Fault current flows in a direction opposite to normal operating current. Reverse-current flow incident to normal operation does not exceed approximately 75 per cent of generator rating. Instantaneous reverse-current protection set at about 125 to 150 per cent of generator rating is recommended in lieu of current-differential protection. Should nondirectional overcurrent protection be employed, it is of the greatest importance to insure that generator circuits are not interrupted as a result of forward current flow incident to heavy motor-starting demand or electric faults external to the generator circuit. The generator-circuit protector should incorporate means for simultaneously de-energizing the generator field to prevent the local generator from continuing to supply current to the fault.

Generator circuits preferably should avoid automatic thermal protection relying on manual supervision to avoid excessive loading. In the presence of excessive load levels, the correct maneuver is to cut off nonessential or semi-important load and most emphatically not to disconnect a generator. To permit reasonable supervision, individual ammeters should be installed in all generator circuits.

Main-feeder-circuit equipment constitutes the class in which adequate fault protection is most difficult of attainment. Excess current magnitude will exist in a number of sections of the main-feeder system as a result of a single fault, even though the latter is in one of the individual load circuits or generator circuits. In many of the main-feeder sections, the direction of fault-current flow may be in either the same or the opposite of normal-current flow, depending on the location of the external fault. In some instances directional-overcurrent protection may be used to advantage but requires inverse-time characteristics to insure correct selectivity.

The attainment of a selective protective system using overcurrent protectors, either with or without current-directional features, relies on the judicious use of inverse-time characteristics. It is to be expected that fault-clearing time will become progressively longer for feeder-system protectors located closer to the generating source busses.

Definite advantages accrue from the use of fault detectors actuated by differential current or ground fault current, rather than by overcurrent. The protectors then are actuated only when the fault lies within that particular section. By this means, all line sections may operate equally fast in response to internal fault, and the tedious time-consuming job of selecting and analyzing the inverse-time properties of the resulting system is eliminated. A system making use of balanced-current protection in transmission channels and ground fault current at junction busses is illustrative of such protection.

Effect of Altitude on Electric Breakdown and Flashover of Aircraft Insulation

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Synopsis: This paper reports a rather extensive investigation made to determine the requirements for insulation clearances for aircraft electric apparatus rated up to 600 volts and intended for use at altitudes up to 65,000 feet. All the tests were conducted on the ground under simulated high-altitude conditions. Tests were made with a variety of materials, including the actual rocker ring of a high-production 28.5-volt d-c generator. A table of recommended striking distances and creepage distances to meet these conditions is given. These recommendations are compared with certain existing standards. It is shown that the clearances and spacings required for high-altitude operation cannot be checked by a sea-level dielectric test without injuring the solid insulation, unless the tests are made in a partial vacuum.

ELECTRIC power to operate auxiliaries is now an indispensable requirement of modern aircraft. Within the past ten years the electric load on an airplane has increased more than tenfold.[1] The optimum voltage for electric systems for large aircraft already has more than exceeded the present 24-volt standard,[2,3] and the use of higher-voltage systems, already tried experimentally,[4] now appears to be a certainty. Moreover, the operating altitudes of modern airplanes, particularly military craft, are climbing steadily to ever increasing heights. Each of these factors indicates a need for a careful study of the requirements for insulation of aircraft electric apparatus in order to achieve minimum weight and space without sacrifice of reliability. A preliminary study of this subject was made and reported[5] prior to the use of the 120-volt a-c system on the *XB*-19, but the projected use of 208-volt systems[4] for still higher altitudes makes a more comprehensive study of this problem most timely.

Atmospheric pressure, temperature, and humidity all affect the insulation clearances required, both over surfaces and through air. The importance of these factors was investigated experimentally for pressure altitudes up to 65,000 feet in order to recommend suitable safe practices for the design of electric equipment, not including ignition systems. Because the electric breakdown strength of solid dielectrics apparently is not affected by altitude, no investigation of this phase of the insulation problem was included. Dirt, oil, and conducting particles obviously affect the flashover strength, and a reasonable allowance for them is made in the safety factors incorporated in the insulation spacings recommended.

Atmospheric Conditions

Considerable data on atmospheric conditions are available in the literature.[6–9] Average barometric pressure decreases regularly with altitude, as shown in Figure 1. Actual temperatures vary over the wide range bounded by the two curves used by the United States Army shown in Figure 2; as a reasonable compromise between these extreme curves, the National Advisory Committee for Aeronautics standard curve was used to represent "standard" conditions throughout the investigations reported in this paper. The stratosphere is the upper region of the atmosphere in which the temperature is practically constant in a vertical direction; according to the NACA curve, the stratosphere begins at 36,000 feet. Air density varies with altitude, as shown in Figure 3, which is a NACA standard curve. Clouds are known to exist at all altitudes up to about 40,000 feet or more. The high cirrus clouds consist of fine particles of ice in suspension, and the lower clouds, where moderate temperatures prevail, consist of droplets of water.[9]

Electric apparatus carried in an airplane is subject to variations of temperature and pressure of the atmosphere. In addition, considerable moisture is to be expected due to condensation or other causes. Moreover, at high altitudes the presence of more ultraviolet and cosmic rays may influence the breakdown slightly. Proper interpretation of the experimental data on this subject requires a review of the principles involved in the breakdown of a gaseous medium.

Fundamentals of Gaseous Breakdown

Spark-over or breakdown between electrodes in gases is much better understood than the phenomenon of the breakdown of liquids and solids. The present knowledge is embodied in the ionization-by-collision theory proposed by Townsend[10] and modified by others. Loeb[11] and Cobine[12] give a comprehensive discussion of this theory as it stands at the present time. The breakdown voltage of a gas is determined primarily by the

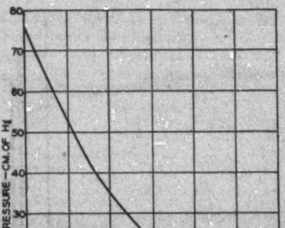

Figure 1. NACA standard pressure–altitude curve

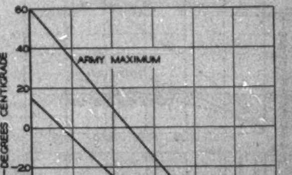

Figure 2. Variation of temperature with altitude

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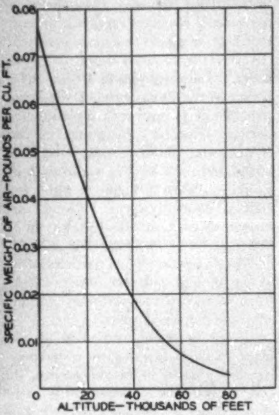


Figure 3. NACA standard altitude-specific weight curve

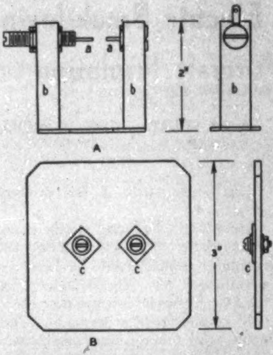


Figure 5. Diagrams of rod gap and sample of insulating material fitted with electrodes

(a). $^1/_{16}$-inch diameter rods
(b). Steatite supports
(c). One-half-inch brass squares—$^1/_{16}$-inch thick

electrode spacing, the gas pressure, the electrode metal and its configuration, the nature of the gas, the frequency of the voltage, and its rate of increase.

Paschen's Law[13] is the guiding principle in all gaseous breakdown work. This law states that, for a uniform field and a given gas, the breakdown voltage is a function of the product (*pd*) of the pressure of the gas and the electrode spacing. Actually, a more nearly correct statement of Paschen's Law would involve the product of density and electrode spacing, rather than the product of pressure and electrode spacing, since density is a function of temperature as well as pressure. The important factor influencing breakdown is not simply the pressure but the total number of molecules between the electrodes. For a given gas and a given electrode configuration the total number of molecules is directly proportional to the density, which is proportional to pressure only if temperature is held constant. However, in this investigation we have taken a series of breakdown tests at varying pressures at a number of different spacings, plotting the results at each different spacing in terms of specific weight which is proportional to density.

The solid lines of Figure 4 give the relationship between breakdown voltage and the product (*pd*) under uniform field conditions for both air and hydrogen. The lower part of this figure is an amplification of the region close to axes of the upper part of the figure. The curves go through a definite minimum, showing that, regardless of electrode spacing or pressure, the minimum breakdown voltage for air under uniform field conditions is about 350 volts. These data were obtained for d-c voltages at normal and reduced pressures and were taken from Schumann's treatise.[14] Data for pressures considerably above atmospheric are given by Howell.[15]

Our work covers the region from sea-level atmospheric pressure down to about 42 millimeters of mercury, which corresponds to an altitude of 65,000 feet. The limits of our *pd* values are indicated approximately by the vertical lines shown in Figure 4. It is thus evident that the region we are concerned with in this paper lies somewhat above the minimum in Paschen's relationship. The region below the minimum is generally referred to as the low-pressure or vacuum region, and that above the minimum as the pressure region, even though part of it is for subatmospheric pressures. In the region we have covered the curves are approximately straight lines. In a rough sense we may say that, for a fixed electrode spacing, the breakdown strength of air is proportional to the density of the gas in the *pd* region between the vertical lines of Figure 4. Thus, the breakdown data for a fixed electrode spacing may be plotted conveniently as a function of the density or pressure if temperature is constant.

The foregoing discussion concerns the case for uniform field conditions between the electrodes. In all of our work we

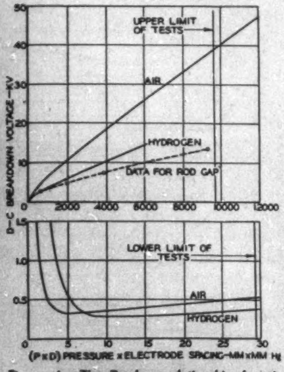


Figure 4. The Paschen relationship for air and hydrogen for uniform fields at 21 degrees centigrade (Schumann)

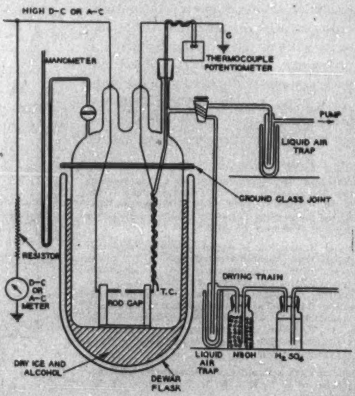


Figure 6. Diagram of apparatus for determining air breakdown and flashover under various atmospheric conditions

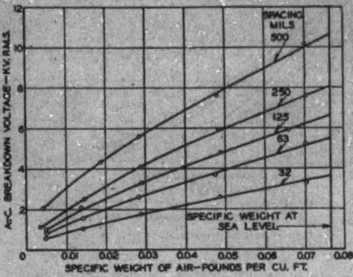


Figure 7 (left). A-c breakdown voltage versus specific weight of dry air

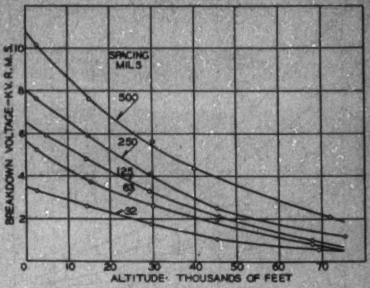


Figure 8 (right). A-c breakdown voltage versus altitude for dry air

have used sharp-edged electrodes of relatively small dimensions in order to simulate more closely practical conditions encountered in aircraft apparatus. With such an electrode system, voltage stress concentrations and nonuniform field conditions prevail. These stress concentrations with the accompanying space charge around the electrodes result in a lower breakdown strength than for uniform field conditions, such as obtained with sphere gaps. For the purpose of comparison, our data on d-c breakdown for a $^1/_{16}$-inch-diameter rod gap with a one-half-inch electrode spacing, determined at 25 degrees centigrade are given by the dashed curve in Figure 4. This curve not only lies below the solid curve for uniform field conditions, but its slope is somewhat less. However, the curve is approximately a straight line, and, even for a rod gap, the breakdown strength is nearly proportional to the density of the gas in the region covered by our work. It should be remembered that Paschen's Law is valid only for uniform field conditions. Thus, the dashed curve of Figure 4 does not apply to any other electrode spacing except the spacing used in determining it.

The discussion so far has been concerned with breakdown of a gas on application of d-c voltage. Aircraft auxiliaries, however, use direct and alternating current at frequencies up to 800 cycles. Therefore, it is important to consider what differences in breakdown strength may be expected between direct and alternating current in the frequency range from 60 to 800 cycles per second. It has been accepted generally that the 60-cycle a-c peak values are the same as the d-c values, especially for large electrode spacings and uniform field conditions. Data are given in this paper for a small-diameter rod gap in which both direct current and 60-cycle alternating current were applied. The comparison between these two sets of data is made in this paper. Since no data were obtained above 60 cycles, it is desirable to determine what may be expected in the 60-to-800-cycle range.

Some valuable data are given on this point by Reukema[16] who worked with a sphere gap in the frequency range of 60 to 425,000 cycles. He found no reduction in breakdown voltage up to 20,000 cycles. At 60,000 cycles the breakdown strength was only 13 per cent below the 60-cycle value for a one-inch electrode separation. The increased percentage of positive ions held in the gap as space charge as the frequency increases is given as the explanation for the gradual decrease in breakdown strength observed above 20,000 cycles. Unfortunately, no data are given for nonuniform field conditions which can be applied directly to our case. However, it appears safe to assume that our data obtained at 60 cycles should be valid up to at least 800 cycles.

Description of Apparatus and Testing Procedure

The work described in this paper may be divided into two parts. The first is concerned with determining the sparking or striking voltage through air gaps of varying lengths under varying atmos-

Table I. Comparison of Flashover Voltages for Various Materials*

Electrode Spacing—One-Fourth Inch; Pressure—300 Millimeters Mercury; Temperature—25 Degrees Centigrade; Dry Air; Sharp-Edged Square Brass Electrodes

	Description of Material		Flashover Voltage—Kilovolts		
Material Designation	Base Material	Surface Treatment	Alternating (Effective)	Alternating (Peak)	Direct
A	Paper-base phenolic-bonded Micarta	2 coats of iron-oxide-filled alkyd resin paint	3.2	4.5	3.8
B	Paper-base phenolic-bonded Micarta	3 coats of air-dried black lacquer	2.8	4.0	3.8
C	Paper-base phenolic-bonded Micarta	2 coats of oil-type clear baking varnish	3.3	4.7	3.9
D	Paper-base phenolic-bonded Micarta	2 coats clear synthetic resin baking varnish	3.5	5.0	4.0
E	Glass-fabric phenolic-bonded Micarta	2 coats special high-temperature varnish	3.5	5.0	3.9
F	Ebony asbestos	None	2.6	3.7	3.9
G	Asbestos-base phenolic-bonded Micarta	None	2.7	3.8	4.2
H	Cloth-base phenolic-bonded Micarta	None	2.8	4.0	3.7
I	Paper-base phenolic-bonded Micarta	None	2.8	4.0	3.9
J	Glass fabric treated with synthetic resin	None	3.0	4.2	
K	Insanol—mica lead borate bonded	None	2.9	4.0	4.7

* These data were obtained at 25 degrees centigrade, and the pressure was chosen, so that they correspond to an altitude of 30,000 feet where the temperature is minus 44.5 degrees centigrade.

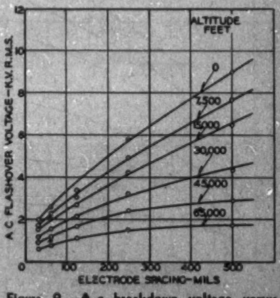


Figure 9. A-c breakdown voltage versus electrode spacing for dry air

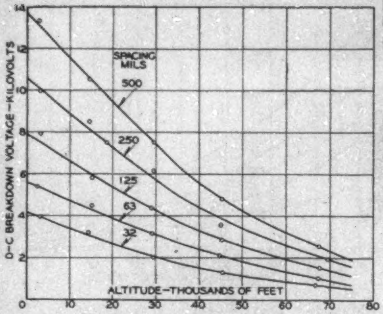


Figure 10 (left). D-c breakdown voltage versus altitude for dry air

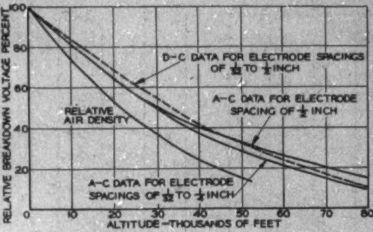


Figure 12 (right). Breakdown voltages at various altitudes in per cent of sea-level values for alternating and direct current

pheric conditions. The second part is concerned with flashover voltages across varying lengths of surfaces of a number of insulating materials, also under varying atmospheric conditions. Since the ultimate purpose of this work is to determine minimum safe striking and flashover voltage distances for aircraft apparatus, the electrodes were chosen to simulate conditions encountered in aircraft electric equipment. The particular electrode arrangement finally chosen for the breakdown tests through air is shown in Figure 5A. The electrodes themselves consist of $^1/_{16}$-inch-diameter brass rods with square ends resulting in sharp edges. These were mounted on steatite supports as shown. The electrode spacing is adjustable, and measurement of the gap over the range from $^1/_{32}$ to one-half inch is accomplished with the help of suitable feeler gauges. A needle gap was considered but rejected, because sharp needle points usually are burned during the first spark-over, resulting in higher values for succeeding spark-overs. The small rod gap appeared to approximate practical conditions sufficiently well without having the serious disadvantage of the needle gap.

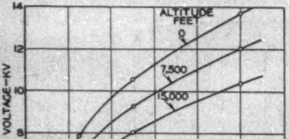


Figure 11. D-c breakdown voltage versus spacing for dry air

In surface flashover tests on various materials, a sharp-edged electrode system is also desirable. A simple arrangement is the one-half-inch brass squares, $^1/_{16}$ inch in thickness, mounted on the surface of the material under test, as shown in Figure 5B, with the corners opposite each other. It should be remembered that, aside from the effect of electrode configuration, the voltage necessary to flashover an insulating surface is primarily influenced by the pressure or density of the gas surrounding the insulating material and by the dielectric constant of the material. In general, the voltage necessary to flashover a certain insulating surface is lower than the voltage necessary to break down a gap of the same electrode separation, because the presence of a solid material distorts the field, particularly near the electrodes. Again it is convenient to plot the data against density or specific weight of the gas surrounding the sample.

In this investigation a pressure range from 40 to 760 millimeters of mercury and temperature range from −55 to +30 degrees centigrade were covered. Furthermore, it was necessary to produce substantially dry air as well as air supersaturated with moisture, so that conditions of clouds, moisture condensation, snow, and ice could be simulated. The apparatus shown in Figure 6 was devised and served to reproduce the various atmospheric conditions desired in a satisfactory manner. The principal part of the apparatus consists of a four-inch diameter Pyrex glass chamber fitted with a ground glass joint which enabled the easy introduction of the rod gap as well as samples of the various materials.

The accessories consist of a manometer, connection to a vacuum pump, two leads sealed through the glass for introducing the high voltage, a thermocouple, and a sulphuric-acid–sodium-hydroxide–liquid-air trap drying train for producing dry air. The major portion of the test chamber is surrounded with a Dewar flask containing a dry-ice–alcohol mixture which is used to obtain the various low temperatures desired. The conditions of cloud, moisture condensation, snow, and ice were produced in the following manner. The drying train shown in Figure 6 is removed, and the side arm of an ordinary one-liter suction flask is connected to the stopcock by means of a rubber tube. Several hundred grams of water are added to the flask. A dense cloud is easily produced above the water surface by adding a little liquid air to the warm water.

Other methods for forming clouds were tried, but none appeared so satisfactory as the water–liquid-air method. When the liquid air comes in contact with the water, it evaporates so rapidly, that it carries minute droplets of water with it into the gas space above the water. After the liquid air is added to the flask, and the cloud starts forming, the main opening to the flask is closed by means of a rubber stopper. The slight pressure developed by the evaporating liquid air makes it easy to transfer the cloud to the test chamber which is opened to the atmosphere by means of a stopcock. In the temperature range from 0 to 30 degrees centigrade, the cloud is relatively persistent over a small range of pressure. From 15 to 0 degree centigrade the cloud condenses on all surfaces, including that of the specimen. A coating of ice is obtained on the surface of the moisture-laden sample by cooling to temperatures below

Table II

Rated Circuit Voltage—Direct or Alternating (Rms)	Class
5 to 50	I
51 to 150	II
151 to 300	III
301 to 600	IV

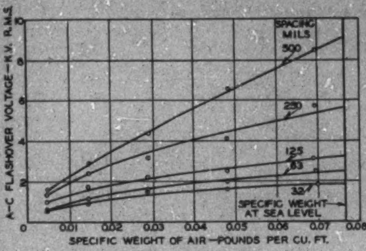
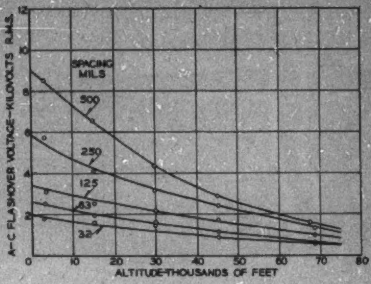


Figure 13 (left). A-c flashover voltage versus specific weight for material *A* in dry air

Figure 14 (right). A-c flashover voltage versus altitude for material *A* in dry air



freezing. Successive layers of ice are obtainable by repeating this process. In the temperature range from 0 to −10 degrees centigrade, the cloud is of short duration, since it condenses rapidly on the walls of the test chamber. When the test chamber is below −10 degrees centigrade, the entering cloud forms snow crystals which slowly precipitate to the bottom of the chamber or on the test piece.

Tests were made with direct current and 60-cycle alternating current. The 60-cycle power supply consists of a small 15-kv transformer having a good wave form. The d-c power supply consists of a well-filtered 30-kv rectifier set of the voltage-doubler type. The poor regulation of either power supply results in a sudden drop of voltage when breakdown occurs. This provides a convenient means for determining when breakdown takes place in cases where the spark or glow could not be observed visually. In the region close to sea-level pressures, both breakdown of the gap and flashover of the samples take place with a spark discharge. In the lower-pressure regions the breakdown and flashover are initiated with a glow discharge. In our investigations either a spark or glow discharge was considered to constitute breakdown or flashover; no distinction between these two types of discharges was made. In all cases the voltage was increased gradually (200 to 500 volts per second) by means of a Variac (variable autotransformer) until breakdown occurred. The voltage was measured by means of a 100-megohm resistor connected in series with a microammeter, as shown in Figure 6. In the a-c tests, the microammeter was of the rectifier type. The resistor-meter combination for both a-c and d-c tests was calibrated by means of a precision voltmeter equipped with a multiplier made of precision resistors.

Most of the data on dry air in both breakdown and flashover tests was obtained at room temperature, pressure being the only variable. The specific weight in pounds of air per cubic foot was then calculated for each pressure. The breakdown as well as flashover voltages were then plotted against specific weight. Figure 7 gives an example of such a plot. Then, by means of Figure 3, which relates specific weight and altitude, this plot can be transferred to a breakdown or flashover voltage versus altitude plot. The data thus are related conveniently to altitude with the temperature and pressure automatically corrected to those corresponding to the NACA standard atmosphere. A few tests were made in which both the temperature and the pressure conditions of the standard atmosphere were reproduced simultaneously. The two sets of data agree within the limits of accuracy of our tests.

In the tests where conditions of moisture condensation, clouds, snow, and ice were simulated, it was necessary to duplicate the standard atmosphere conditions of temperature and pressure rather closely. The temperature corresponding to a given pressure could not always be held at an exact value. Corrections to the specific weight for small temperature changes were made by assuming the air to be dry. This assumption is justified, because at no temperature nor pressure in the standard atmosphere is the partial pressure of the water vapor greater than 1.5 per cent of the total pressure.

Experimental Data and Discussion

As previously stated, much of the data given in this paper was obtained at room temperature on the assumption that density or specific weight is the primary factor affecting breakdown of a gap and flashover of a material for dry-air conditions. However, some parallel tests were made in which the conditions of both

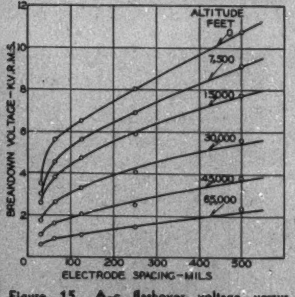
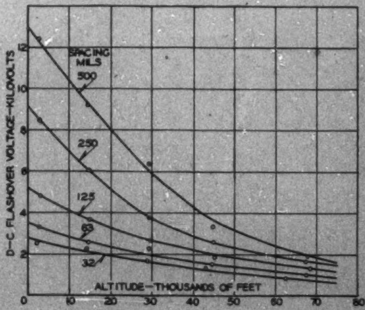


Figure 15. A-c flashover voltage versus spacing for material *A* in dry air

Figure 16. D-c flashover voltage versus altitude for material *A* in dry air



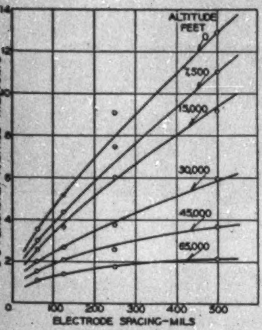


Figure 17. D-c flashover voltage versus electrode spacing for material *A* in dry air

Table III. Proposed Minimum Striking and Creepage* Distances for Aircraft Electric Apparatus**

Circuit Voltage—Direct or Alternating (Rms)	Up to 40,000 Feet: Distance—Inches	Up to 40,000 Feet: Observed Breakdown Volts (Rms)	Up to 40,000 Feet: Factor of Safety	Up to 60,000 Feet: Distance—Inches	Up to 60,000 Feet: Observed Breakdown Volts (Rms)	Up to 60,000 Feet: Factor of Safety	Railway Apparatus Standards	Underwriters' Standards: General Industrial Control	Underwriters' Standards: Small Controls (One Horsepower)
Striking Distance									
5–50	1/32	1,250	25	1/32	700	14	1/32		
51–150	1/32	1,250	8.3	1/16	1,100	7.3	1/16	1/8**	1/16**
151–300	1/16	2,100	7.0	1/8	1,350	4.5	1/8	1/4	3/16**
301–600	1/8	2,450	4.1	3/8	2,100	3.5	1/4	3/8	3/16**
Creepage Distance									
5–50	1/16	1,300*	26	1/16	850*	17	5/64		
51–150	1/16	1,300*	8.7	1/8	1,250*	8.3	13/64	1/4	1/8**
151–300	1/8	1,800*	6.0	1/4	1,700*	5.7	23/64	3/8	3/8
301–600	1/4	2,700*	4.5	1/2	2,000*	3.3	5/8	1/2	3/8

* New, clean, dry, insulation.

** Underwriters say, "The spacing between wiring terminals of opposite polarity shall be not less than one-fourth inch in any case, if the terminals are in the same plane."

*** Not applicable to bar-to-bar spacings of commutators. The materials employed for insulation and the mechanical rigidity of the assembly are such as to permit a much lower factor of safety on bar-to-bar spacing. Actual bar-to-bar spacings vary from 0.018 to 0.032 with voltages usually in the order of five to ten volts per bar.

temperature and pressure corresponding to the standard atmosphere were reproduced in an attempt to show that temperature has an effect only as it affects the specific weight. All of the data for air containing moisture in any form were obtained under conditions of actual temperature and pressure closely corresponding to the standard atmosphere, because the moisture content of saturated air depends on temperature. Both d-c and 60-cycle a-c tests were made in many cases in order to compare the effects of the two types of voltages. Each plotted point for dry air is the average of from five to ten individual determinations. The spread in individual values was generally less than 10 per cent; however, in some cases a spread as high as 25 per cent was observed. In the tests involving moisture, the spread was, in general, greater than for dry air, and the values for individual determinations were plotted.

BREAKDOWN TEST RESULTS ON ROD GAP WITH DRY AIR

Figure 7 gives the results for dry air obtained at 60 cycles plotted against specific weight for five electrode spacings varying from 0.032 to 0.50 inch. These data were transferred to the altitude plot shown in Figure 8 using the curve in Figure 3. Figure 9 shows the same data plotted against electrode spacing with altitude as parameter. Figures 10 and 11 give similar data for d-c tests, except that the specific weight plot is omitted in this case. The peak of the a-c effective values given in Figure 8 compare quite favorably with the d-c values of Figure 10. Thus, it can be concluded that there are no significant differences between the 60-cycle peak and d-c breakdown voltages for the rod gap.

All of the data for the rod gap are summarized in a convenient and useful manner in Figure 12, where the breakdown voltages at various altitudes are expressed in per cent of the sea-level values. Here again it is apparent that the a-c and d-c relative values are in satisfactory agreement. The d-c values for the different electrode spacings are in such close agreement, that all of these data are represented by the dashed curve. The a-c values for electrode spacings require two curves as shown, but these are fairly close together. These curves are particularly useful in determining what breakdown voltages may be expected at various altitudes when only sea-level test data are available. Conversely, they show what voltage to impress at sea level to duplicate the effect of any given voltage at a higher altitude.

SURFACE FLASHOVER TEST RESULTS FOR VARIOUS MATERIALS IN DRY AIR

A number of insulating materials commonly used in electric apparatus were investigated. These are described in Table I. This table also gives the flashover values for a-c and d-c voltages with an electrode spacing of one-fourth inch and at an altitude of 30,000 feet. No significant differences are apparent among the various materials in respect to their flashover behavior in dry air. It should be noted that, in nearly all cases, the a-c peak values are somewhat higher than the d-c values. However, the differences in most cases are not large enough to attach important significance to them. When these flashover values are compared with the breakdown values in Figures 8 and 10 for the corresponding one-fourth-inch electrode spacing and 30,000 feet altitude, it

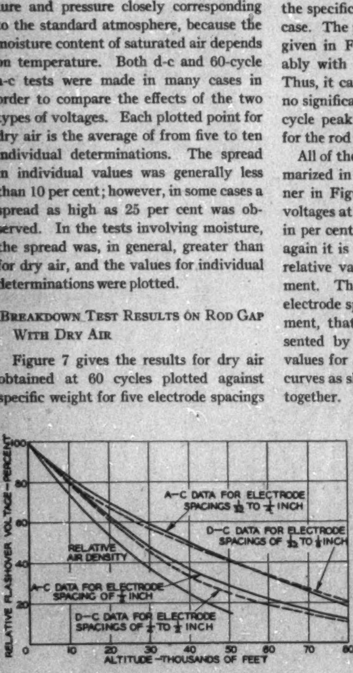
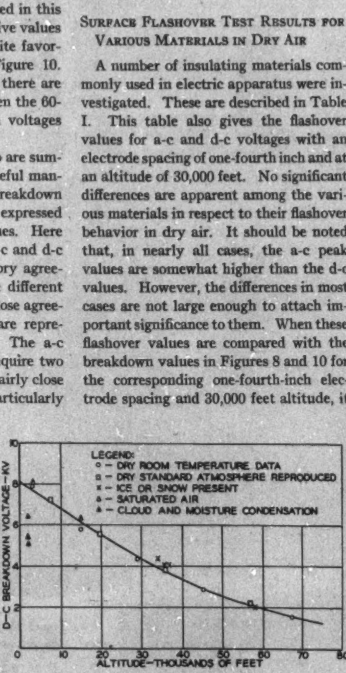


Figure 18 (left). Flashover voltages at various altitudes in per cent of sea-level values for alternating and direct current

Figure 19 (right). D-c breakdown voltage versus altitude for one-eighth-inch rod gap under various atmospheric conditions



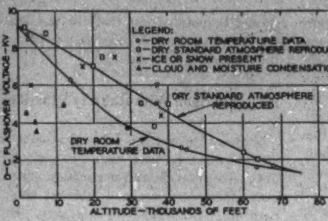


Figure 20. D-c flashover voltage versus altitude for material *A* with one-fourth-inch electrode spacing under various atmospheric conditions

is found that the flashover values are roughly 25 per cent lower than the corresponding breakdown values, except for small spacings where the discrepancy is greater. This result is what would be expected and emphasizes the need for allowing a greater flashover or creepage distance than striking distance for a given voltage. This comparison, however, cannot be placed on a strictly quantitative basis, for the electrode configuration is somewhat different in the two cases.

Material *A* of the list given in Table I was investigated more completely over ranges of electrode spacing and altitude. Figure 13 gives the a-c data for a series of electrode spacings plotted against specific weight. From these curves the family of flashover voltage versus altitude curves of Figure 14, as well as the family of flashover voltage versus electrode spacing curves of Figure 15, are derived. Similar families of curves are given for d-c test voltages in Figures 16 and 17. The peak values of the a-c data of Figure 14 agree quite well with the d-c values of Figure 16. This indicates that there are no significant differences between the 60-cycle peak values and the d-c values over the range of altitude and electrode spacing covered in this work.

The data on flashover voltages for material *A* are summarized in a convenient form in Figure 18. The relative flashover voltages for both a-c and d-c voltages are given in per cent of the sea-level values. There is good correlation between the a-c and d-c relative flashover values but not such good correlation between small and large gaps. The explanation for the discrepancy between the larger and the smaller electrode spacings is not clear. In any case, no very great accuracy should be ascribed to flashover tests. It is interesting to note from a comparison of Figures 12 and 18, that the relative breakdown voltage for the rod gap decreases with altitude at nearly the same rate as the flashover voltage. This is another indication that surface flashover under dry conditions is influenced primarily by the density of the air surrounding the material under test.

BREAKDOWN TEST RESULTS ON ROD GAP WITH MOISTURE-LADEN AIR

All breakdown tests with moisture-laden air were made using d-c voltage only. The results obtained are plotted as individual points in Figure 19. The curve represents the data for dry air which are reproduced for the purpose of comparison. The points enclosed by the circles are for the one-eighth-inch electrode spacing which were taken from Figure 10, the data for which were obtained at room temperature. The points enclosed by squares are also for dry air but were obtained by reproducing the actual temperature and pressure conditions of the standard atmosphere. The close agreement between the two sets of points indicates that the temperature affects the breakdown of a given air gap only through its effect on density.

When either snow or ice particles are suspended in the air between the electrodes it is interesting to note that no lower breakdown voltages were obtained than with dry air under otherwise identical conditions. The same is true for air saturated with moisture, as is indicated by the two points enclosed with the open triangles in the lower-altitude region of Figure 19. This is in qualitative agreement with the work of Fielder[16] who actually found some increase in breakdown strength with increase in moisture content of the air. This increase, however, was only slight for the smaller electrode spacings. On the other hand, when moisture is present in the form of liquid droplets or dense visible cloud, the breakdown of the rod gap is lowered. This is indicated by the solid triangular points shown in the 2,000-foot-altitude region of Figure 19. Thus, it may be concluded that moisture in the frozen state has no appreciable effect on the breakdown strength, and that suspended moisture in the liquid droplet form appreciably lowers the breakdown strength of the rod gap.

SURFACE FLASHOVER TEST RESULTS FOR VARIOUS MATERIALS IN MOISTURE-LADEN AIR

All flashover tests on materials subjected to moisture-laden air were made at one-fourth-inch electrode spacing and with d-c voltages only. Although only three materials were investigated, a reasonably good picture of the behavior of flashover of surfaces in the presence of moisture was obtained. The results for material *A* are presented in Figure 20. The data for dry air obtained at room temperature are given for purposes of comparison. The tests on dry air were repeated under conditions in which the temperature and pressure conditions corresponding to the standard atmosphere were closely simulated. These data are represented by the upper solid curve and the somewhat scattered points enclosed by squares. Coincidence of the two curves was expected, and, therefore, the reason for this anomalous behavior is not entirely clear, except that temperature must in some way directly or indirectly affect the results. However, it can be said that all of the data for dry air obtained at room temperature are at least on the conservative side, and no difficulty should be encountered in using them.

The crosses shown in Figure 20 represent the data obtained with snow or ice deposited on the surface of the material. These results show that moisture in the frozen form results in no lower flashover voltage than that obtained with dry air. On the other hand, when there is liquid moisture condensed on the surface of the material, and when there is a visible cloud

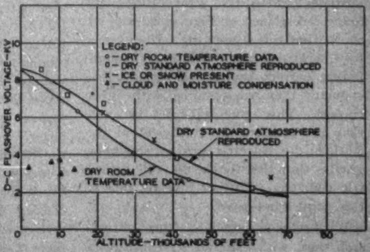


Figure 21. D-c flashover voltage versus altitude for material *E* with one-fourth-inch electrode spacing under various atmospheric conditions

in the air surrounding the material, the flashover is lowered by 50 per cent or more. The data obtained under these conditions are given by the solid triangular points shown in the lower-altitude region of Figure 20.

A similar set of data for material *E* are given in Figure 21. Again, there are the two curves which represent the data obtained with dry air by the two different methods. The discrepancy between the two curves is not so great as that observed for material *A*. The effects of moisture with this material are very similar to those observed for material *A*.

Figure 22 gives the results for material *K* obtained under conditions similar to those used for the previous two materials. It is at once apparent that no discrepancy exists between the data for dry air obtained at room temperature and those obtained when the standard atmosphere conditions were reproduced. The set of points enclosed by circles and those enclosed by squares all lie close to the same smooth curve. The differences in behavior of these three materials give some clue as to a possible cause of the discrepancy observed with materials *A* and *E*.

The laminated phenolic materials, to which class of materials *A* and *E* belong, are known to have a relatively large positive temperature coefficient of dielectric constant. The dielectric constant of material *K* or Insanol, on the other hand, has a much smaller temperature coefficient. The greater the dielectric constant of a material, the greater is field distortion, and the lower is the flashover voltage. If this is true, then one would expect a considerably lower dielectric constant for materials *A* and *E* when the low temperatures of the standard atmosphere were simulated than when the tests were carried at room temperature. Hence, higher flashover voltages should follow when the tests are carried out at low temperatures.

The chief objection to this theory is that the discrepancy between two procedures should be greater at the higher altitudes where the temperatures are lower. A glance at Figures 20 and 21 shows that this is not true. However, a few check tests made with 60-cycle alternating current revealed a far less discrepancy than was obtained with direct current. This agrees with the theory in that the temperature coefficient of the static or d-c dielectric constant should be greater than that for the 60-cycle dielectric constant. Further investigation of this point is indicated.

With reference again to Figure 22, it is seen that, with material *K*, moisture in the frozen form also does not produce any lowering of the flashover values, as was the case with the other two materials. Liquid moisture condensed on the surface and suspended in the air produced a very marked lowering of the flashover voltage. The results of this work indicate that moisture condensation can produce flashover voltages at low altitudes which are as low or lower than those for an altitude of 40,000 feet. However, if the creepage distances are designed to be adequate for an altitude of 60,000 feet, it appears that there should be little likelihood of a flashover occurring because of moisture condensation. No positive statements can be made as to differences among various materials in their behavior under conditions of moisture condensation. Although very little tracking was observed in these tests, some of the organic materials, of course, may become tracked under surface flashover conditions when a higher energy power source is used than was the case in our experiments. Inorganic materials like Insanol (material *K*) do not track, but their use is limited because of the difficulties encountered in fabrication.

RESULTS OF TESTS ON AIRCRAFT-GENERATOR BRUSH-HOLDER ASSEMBLY

In order to obtain some idea of the agreement between tests made on laboratory samples and those made on a practical piece of apparatus, a brush-holder assembly for a 28-volt aircraft generator was investigated. The assembly consists of eight brushes mounted on a ring of insulating material *G*, which is described in Table I. The closest striking distance ($^7/_{64}$ inch) is between a brush spring and an adjacent brass brush holder. The d-c breakdown voltages for this air space under dry-air conditions are plotted against altitude in Figure 23. The shortest creepage distance ($^{19}/_{64}$ inch) is between mounting flanges of adjacent brush holders. The d-c flashover voltages for this creepage distance under dry-air conditions also are given in Figure 23.

On comparing the breakdown-voltage data of Figure 23 (lower curve) with those for the rod-gap set at the 125-mil spacing, the agreement is found to be close. A similar comparison of the flashover-voltage data given in Figure 23 with those given in Figure 16 shows that the former are somewhat higher than the latter. This is to be expected, since, in the laboratory samples, sharp-edged electrodes were used; whereas, the edges of brush-holder flanges were rounded appreciably. While the agreement between tests on laboratory samples and those on a piece of apparatus is very satisfactory, it should be pointed out that the tests on the laboratory samples are likely to be on the conservative or safe side.

A Comparison With Railway Practice

Railway apparatus is subjected to service most nearly comparable to aircraft duty, except for the extreme reductions in atmospheric pressure and ambient temperature. Both types of apparatus are subjected to moisture, vibration, and dirt. Consequently, the experience with test voltages, striking and creepage distances on railway apparatus, was examined to determine what factors of safety have been proved acceptable by successful operation. Furthermore, on railway equipment space and weight are at a premium (as on aircraft), and the tendency has been to keep down size by reducing clearance between live parts. A 250-volt application was selected for comparison. Standard practice has been to use one-eighth inch as a minimum striking distance and $^5/_{16}$ inch as a minimum creepage distance over surfaces. The one-eighth-inch air-gap distance breaks down at 6,500 volts (rms) with a rod gap at zero altitude. The $^5/_{16}$-inch creepage surface breaks down at 8,300 to 9,800 volts (rms) when dry (eight different materials). When the surface is moist, these same materials break down at 2,700 to 4,200 volts rms. The normal insulation test for such an application is 2,000 volts rms ($2E+1,500$). From this it can be seen that the successful background of experience on this class of apparatus is based on the following:

(*a*). Test for new apparatus is $8\times E$ or $2E+1,500=2,000$ volts, where E is the rated voltage of the apparatus.

(*b*). The minimum permissible striking distance breaks down at $3^1/_2$ times the normal test voltage, or $26\times E$.

(*c*). On dry surfaces the minimum permissible creepage distance breaks down at somewhat more than four times the normal test voltage or $32\times E$.

(*d*). On moist creepage surfaces failure may occur at a voltage 30 per cent above the normal test voltage (depending on the character of the surface), which is $10\times E$ (minimum observed).

While comparable spacings may be allowed for aircraft apparatus, and comparable factors of safety thus obtained while on the ground, these factors of safety must be reduced at the higher altitudes in accordance with Figures 12 and 18. To obtain a breakdown voltage of 4,000 volts at 65,000 feet would, according to Figure 9, require a spacing well in excess of one inch, a rather impractical figure.

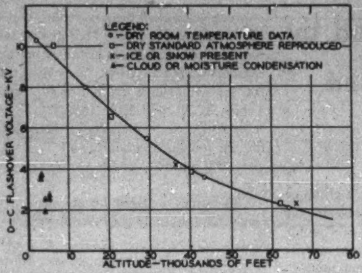


Figure 22. D-c flashover voltage versus altitude for material *K* with one-fourth-inch electrode spacing under various atmospheric conditions

Recommended Insulation Spacings

In establishing proposed standards for insulating and testing practice, it appears desirable to segregate apparatus by voltage classes. The classifications proposed are given in Table II.

Much of the apparatus now designed is being used up to altitudes of 40,000 feet. Future needs of the industry appear to include operation up to a 60,000-foot altitude. It is suggested that two different altitude classifications be established, as the use of a single class based on 60,000 feet might unduly penalize certain types of existing apparatus or apparatus not intended to be used above 40,000 feet.

Proposed standards for minimum striking and creepage distances are given in Table III, which also shows the experimental "factor of safety." The factors of safety are based on tests at the specified altitudes for the recommended spacings. These factors of safety are high for the lowest-voltage class, partly because it is usually unsafe to rely on extremely small spacings and partly because of the increased danger of filling the space with particles of conducting dirt or water. As a matter of interest, comparative standards for railway apparatus, also standards of the Underwriter's Laboratories for Industrial Control, are given. The standards recommended for aircraft apparatus are the authors' personal evaluation of the experimental results obtained, with the practical side in mind. As the factors of safety indicate, it sometimes may be satisfactory to use smaller clearances, particularly if the apparatus is enclosed to exclude dirt and so rigid mechanically as to prevent any relative movement between the two parts at different potentials. In other cases it may be necessary to use greater clearances than indicated.

Dielectric Test of Insulation

Electric apparatus is customarily given an overpotential test to check the adequacy of the insulation for all normal voltages plus transient overvoltage surges. By insulation is meant solid insulation, clearance through air, and creepage over surface. If a sea-level dielectric test is to prove electrically that the spacings in the device are adequate for operation at 40,000 feet, for example, the test voltage must be just under that value necessary to break down the gaps specified in Table III. These voltages are from 3,500 to 6,600 volts rms. Such a requirement would unduly penalize the design by requiring an unnecessary amount of solid insulation. The obvious conclusion, then, is that no reasonable sea-level dielectric test can check the spacings and clearances. In other words, the sea-level dielectric test voltage must be selected from the standpoint of solid insulation. To check the spacings electrically would require that the dielectric tests be conducted in a partial vacuum.

Summary and Conclusions

The breakdown strength of air and the surface flashover behavior of a number of materials have been investigated under conditions corresponding to an altitude range from sea level to 65,000 feet. The various atmospheric conditions encountered by aircraft in this altitude range were simulated by use of a special test chamber. The more important results of this investigation are as follows:

1. Moisture in the frozen form, such as ice and snow, does not lower either the breakdown voltage of air or the flashover voltage of an insulating surface.

2. Moisture in the vapor form does not have a significant effect on the breakdown strength of small air gaps ($^1/_{32}$ to $^1/_2$ inch) or the flashover of insulating surfaces, provided condensation is absent.

3. Moisture in the cloud or suspended droplet form lowers the breakdown strength of small air gaps. Moisture condensation on the surface of an insulating material results in a marked lowering of the flashover voltage.

4. Some differences were observed in the flashover behavior of various materials, particularly at low temperatures and with d-c voltages. Dielectric constant changes with temperature are believed to play a role. These differences in materials, however, are small compared with the safety factors that must be allowed in practical designs.

5. The overpotential test voltages necessary at sea level to determine the adequacy of striking and creepage distances for high-altitude operation may result in excessive stresses on the solid insulation. This can be avoided by conducting such tests in a partial vacuum.

6. On the basis of the data obtained and general experience with various types of electric apparatus, specific striking and creepage or flashover distances have been proposed which are believed to be adequate for the electric equipment used in aircraft.

7. As shown in Figures 12 and 18, both the breakdown and flashover voltages decrease less rapidly with altitude than does the relative air density. This is to be expected because of the nonuniform field conditions used in this work.

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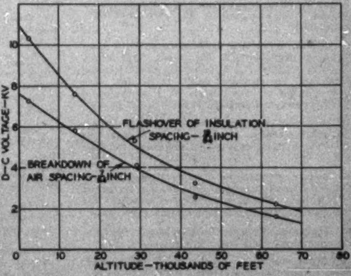

Figure 23. D-c breakdown and flashover voltage versus altitude between adjacent brush holders of d-c aircraft-generator brush-holder assembly

Current Rating of Cables as Affected by Mutual Heating in Air or Conduit

IPCEA COMMITTEE ON RESEARCH

THE current rating of a cable is determined by its surroundings in addition to its inherent characteristics. The cable may be exposed in air, drawn into a conduit in the air, buried directly in the ground, or drawn into a duct in the ground. It is rarely necessary to consider a single cable, that is, it is customary to install a number of cables in a group. Thus, instead of a single cable dissipating its heat to the surroundings, the problem becomes more complex because of the effect of the heat being dissipated from each cable on all of the cables in the group. Therefore, the current rating for a single cable must be modified. For cables drawn into duct systems, duct heating constants obtained from experimental data are added to the thermal resistance of a single cable.$^{1-3}$ In the case of buried cables, there is a similar term added to the thermal resistance of a single cable for each additional cable. For cables in air or in conduit in air, no experimental data have been available to show the influence the grouping of the cables or the conduits have on the current rating of the cable. Correction factors for the rating of a single cable in air or when installed in a conduit have been estimated theoretically, but lack of confirmatory data made assumed values of doubtful accuracy.

The Insulated Power Cable Engineers Association recognized this situation several years ago, and in July 1941 an appropriation was obtained from a group of manufacturers to carry out a research on the effect of mutual heating of a group of conduits for different numbers of conduits and various geometric groupings.

The research was carried out by the Electrical Testing Laboratories under the supervision of a committee from the IPCEA with certain supplemental data from the laboratories of one of the subscribing companies.

The research consisted of taking one- and four-inch standard conduits in groups of 1, 3, 6, and 9 with standard spacing between conduits, inserting in each conduit a twisted pair of number 19 American wire-gauge hotbed heater wires, and applying various amounts of heat (watts per foot) to obtain a curve of temperature rise of the conduit above ambient temperature. As it is important to know the effect on the heat dissipating properties of the conduits when conduits are installed close to a wall, near ceilings, or in completely covered runways, tests were made with conduits freely open to the air, with barriers of paper board about four-feet high, spaced about two feet from the conduit, and with barrier and covers, the latter about two feet above the conduits. These three conditions simulated free dissipation of heat, side restriction but vertical dissipation, and an enclosure restricting heat dissipation. Details as to the test methods, taken from the report of the ETL will be found in Appendix A.

Many data have been collected, which have been reported by ETL, but it is not possible to include them in this paper because of space limitation. A typical test run for a four-inch conduit is shown in Tables X, XI, and XII, test 26.

Table I gives a summary of the results for the four-inch conduit, and Table II gives the summary of the results for the one-inch conduit.

The results obtained for the four-inch conduit are shown in the following figures:

Figure 1 (ETL plate 36,391). A single-conduit bank compared with the center conduit (5) hottest of a nine-conduit bank.

Figure 2 (ETL plate 36,464). Various groupings with banks isolated (barriers) and covered.

Figure 3 (ETL plate 36,605). Various groupings with banks isolated but not covered.

Figure 4 (ETL plate 36,606). Various groupings with banks open, that is, no barriers or covers.

The effect of grouping is very definite for a given watts-per-duct foot, but the differences between open (that is, no barriers or covers), isolated (barriers) and covered, and barriers and not covered, are not very great.

The results for the one-inch conduit are shown in the following figures:

Figure 5 (ETL plate 36,513). A single conduit compared with the center conduit (5) hottest of a nine-conduit bank.

Figure 6 (ETL plate 36,607). Various groupings with banks open (that is, no barriers or covers).

Figure 7 (ETL plate 36,608). Various groupings with banks isolated (barriers) and covered.

Here, also, the effect of groupings is very definite for a given watts-per-duct

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Personnel of the IPCEA committee on research: R. J. Wiseman, *chairman;* W. A. Del Mar, S. J. Rosch, and G. B. Shanklin.

Investigation was made by the Electrical Testing Laboratories for a group of electrical wire and cable manufacturers under the supervision of the IPCEA.

The IPCEA committee on research expresses thanks for contributions of discussions on the interpretation of the data to R. C. Waldron, The Okonite Company; F. H. Buller, General Electric Company; J. H. Palmer and H. Philp, Phelps Dodge Copper Products Corporation; and L. F. Roehmann, Anaconda Wire and Cable Company; and to G. Thompson, chief engineer, Electrical Testing Laboratories, for his interest in this investigation.

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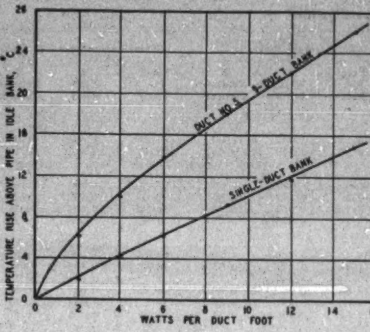


Figure 1 (left). Temperature rise of four-inch black steel conduit

Banks "open." One- and nine-conduit banks only under test

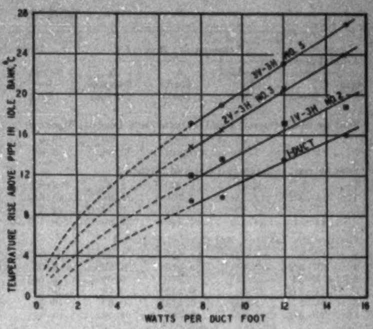


Figure 2 (right). Temperature rise of four-inch black steel conduit

Banks isolated and covered

foot, and the differences between open, barriers and covered are not great.

The matter of drawing conclusions from the data may be approached in two ways:

(*a*). The number of watts of energy allowable in each conduit in a group that will result in the same temperature rise for the conduit giving the highest temperature.

(*b*). The temperature of the hottest conduit for a given number of watts of energy.

The allowable conductor temperature in a cable determines its current rating. Therefore, it is obvious that the watts per foot of conduit must decrease as the number of conduits in a group increases to give the same temperature rise of the conductor. It was decided, therefore, to study the variations in the number of watts-per-conduit foot for the same conductor temperature rise.

Tables IIIA and IIIB show the number of watts per foot for various groupings of conduit for the same temperature rise for both one- and four-inch conduits and the ratio of watts per foot for a group to the watts per foot for a single conduit for a given temperature rise.

These data disclose some interesting information, provided that we do not try to be too exact. This will be found to be justified when one considers the manner in which the data will be used:

A. There is not a great deal of difference, in the ratio of the watts per foot for a group to the watts per foot for a single conduit for a given arrangement of conduits and a specific temperature rise, between the conduits in the "open," "isolated," or "isolated and covered." This fortunately simplifies the problem, because it can be assumed that all are alike, and, therefore, the same correction will apply for all cases.

B. There is an increase in the ratio with an increase in the temperature rise, undoubtedly due to the increase in air-convection currents. This, too, is helpful as the ratio for, say, a ten-degree-centigrade rise may be taken, when it is known that, if the cable is designed to give a higher temperature rise of the conduit, the values for grouping based on a ten-degree-centigrade rise will result in conservative ratings.

C. There is not much difference between the ratio for a given case for the one- and four-inch conduits for the conditions of installation, namely, the standard spacing of conduits. This is helpful, as it means that the factors set up are applicable to any size conduit.

D. It is obvious that, as the spacing between conduits is increased, the influence of conduits on each other will decrease. These tests probably represent the closest spacing likely to be encountered. Larger spacings will permit higher currents and, if conditions warrant, should be made the subject of special investigation.

E. As one wishes to make it as simple as possible in calculations, average values must

Table I. Summary—Temperature Rise of Hottest Conduit Above Idle Conduits—Banks "Isolated"

Four-Inch-Conduit Tests (Final Series), All Banks

Test Run	Watts Per Foot	Temperature Rises (Degrees Centigrade)					
		Ducts Per Bank					
		1	2V–3H	3V–2H	3V–3H	3V–1H	1V–3H
Barriers Between Banks But Banks Not Covered							
17	15.0						17.4
16	15.0		20.4				
19	15.0	13.3		20.9			18.7
22	15.0	14.8		21.0		20.9	
(*c*)...29	15.0	14.2	20.3		26.4		18.6
18	12.0			18.1			15.7
15	12.0		16.6			18.3	
21	12.0	11.5		17.3		17.6	
20	9.0	9.4		13.9		13.7	
23	9.0	8.5	12.9				11.6
(*d*)...28	7.5	7.3	12.0		16.5		10.4
Barriers Between Banks and Banks Covered							
(*a*)...26	15.0	16.1	24.0		27.0		19.0
25	12.0	13.5	20.5		23.0		17.0
24	9.0	9.9	16.6		18.9		13.6
(*b*)...27	7.5	9.5	14.9		17.2		12.0
Barriers Removed and Banks Not Covered							
30	15.0	12.5	18.9		24.0		17.3
31	7.5	7.4	11.0		14.0		10.4

The letters *a*, *b*, *c*, and *d* designate the runs for which the detailed data will be found in Tables X, XI, and XII.

Table II. Summary—Temperature Rise of Hottest Conduit Above Idle Conduits—Banks "Isolated"

One-Inch-Conduit Tests, All Banks

Test Run	Watts Per Foot	Temperature Rises (Degrees Centigrade)					
		Ducts P.r Bank					
		1	2V–3H	3V–2H	3V–3H	3V–1H	1V–3H
Banks Open, That Is, No Barriers or Covers							
32	3.0	8.6	12.7		14.9		11.4
33	7.0	16.5	23.9		29.9		22.4
34	11.0	24.4	34.5		41.1		31.5
35	3.0	8.6		11.3		12.7	
36	7.0	16.4		23.0		24.9	
37	11.0	24.8		34.6		35.7	
With Barriers, Banks Not Covered							
38	11.0	27.2	35.3		42.1		31.3
With Barriers, Banks Covered							
39	3.0	10.0	13.1		15.9		11.6
40	7.0	20.5	26.9		31.6		22.6
41	11.0	29.7	38.1		43.6		33.1

Table IIIA. Four-Inch-Conduit Temperature Rise

Arrangement		Temperature Rise = 5 C			Temperature Rise = 10 C			Temperature Rise = 15 C			Temperature Rise = 20 C		
Number	Block	Watts Per Foot	A Watts Ratio	B Relative Current	Watts Per Foot	A Watts Ratio	B Relative Current	Watts Per Foot	A Watts Ratio	B Relative Current	Watts Per Foot	A Watts Ratio	B Relative Current
Four Inches, "Open," 1 and 9 Ducts Only Loaded, Figure 1													
1		4.65	1.000	1.00	9.9	1.000	1.00	15.15	1.000	1.00	(20.4)	1.000	1.00
9	3*V*-3*H*	1.45	0.312	0.56	3.85	0.389	0.62	6.95	0.458	0.68	10.55	0.517	0.72
Four Inches, Isolated and Covered, Figure 2													
1		(3.2)	1.000	1.00	8.05	1.000	1.00	13.8	1.000	1.00	(19.45)	1.000	1.00
3	1*V*-3*H*	(2.25)	0.704	0.84	(5.85)	0.726	0.85	10.3	0.746	0.86	15.0	0.771	0.88
6	2*V*-3*H*	(1.65)	0.515	0.72	(4.3)	0.534	0.73	7.75	0.561	0.75	11.75	0.604	0.78
9	3*V*-3*H*	(1.15)	0.359	0.60	(3.15)	0.391	0.63	(6.2)	0.450	0.67	9.86	0.506	0.71
Four Inches, Isolated, Not Covered, Figure 3													
1		(4.55)	1.000	1.00	10.05	1.000	1.00	16.15	1.000	1.00	22.25	1.000	1.00
3	1*V*-3*H*	(3.05)	0.670	0.82	7.05	0.702	0.84	11.65	0.721	0.85	16.6	0.746	0.86
3	1*H*-3*V*	(2.15)	0.473	0.69	(5.55)	0.552	0.74	9.65	0.597	0.77	14.1	0.634	0.79
6	2*V*-3*H*	(2.6)	0.571	0.76	(6.2)	0.617	0.80	10.35	0.641	0.80	14.85	0.667	0.82
6	3*V*-2*H*	(2.15)	0.473	0.69	(5.55)	0.552	0.74	9.65	0.597	0.77	14.1	0.634	0.79
9	3*V*-3*H*	(1.15)	0.253	0.50	(3.45)	0.343	0.59	6.55	0.406	0.64	10.05	0.452	0.67
Four Inches, "Open", Figure 4													
1		(4.6)	1.000	1.00	11.25	1.000	1.00	18.95	1.000	1.00	(26.75)	1.000	1.00
3	1*V*-3*H*	(2.95)	0.641	0.80	7.3	0.649	0.81	12.45	0.657	0.81	18.1	0.677	0.82
6	2*V*-3*H*	(2.65)	0.576	0.76	(6.6)	0.587	0.77	11.2	0.591	0.77	16.25	0.608	0.78
9	3*V*-3*H*	(1.95)	0.424	0.65	(4.85)	0.431	0.66	8.25	0.435	0.66	11.95	0.447	0.67

be selected in such manner that any errors are on the conservative side. Also, they should be applied easily.

The data which have been obtained give the temperature of the conduit as compared with a base temperature or the temperature rise of the conduit due to various loadings inside the conduit. Therefore, any factor that is obtained for groups of conduits, as compared with a single conduit, applies only to the temperature drop from the conduit to the ambient temperature. This factor is in the nature of a thermal resistance. It may be an additional term to be added to the total thermal resistance of a cable in a conduit, or it may be converted to a correction factor to the conduit factor Q, which had been determined previously by a committee of National Electrical Manufacturer's Association and presented by S. J. Rosch before AIEE.[4] Values of Q have been adopted by IPCEA and may be found in their current-carrying capacity tables for rubber-insulated and varnished-cambric-insulated cables.

If a multiplying group correction factor for Q is used instead of an additive factor, it simplifies the work and results in a representative average value slightly on the safe side. The formula for thermal resistance then may be written as

$$R = R_i + R_s + GQ \tag{1}$$

where

R_i = thermal resistance of insulation

R_s = thermal resistance of surface of cable to conduit

Q = conduit correction factor as given in IPCEA tables

G = group correction factor

The value of G must be determined from the test data. Calculations indicate that a ten-degree-centigrade temperature rise for the one-inch conduit and a 15 degree-centigrade temperature rise for the four-inch conduit are average values for a fully loaded cable in the respective conduit sizes. Data, therefore, are taken from the test results for these temperature rises and tabulated in Table IV.

The value of G listed is the ratio of the watts per foot for a single conduit ($1V \times 1H$) to the number of watts per foot for various groupings under the same conditions of installation. Although these data are rather limited in extent, it is possible to draw curves with them to obtain values for any grouping. Figure 8 shows the plotting of these data for four-inch conduit in two ways:

(*a*). The correction factor G as ordinates against the number of vertical conduits as abscissa for a specific number of horizontal conductors.

(*b*). G as ordinates against the number of horizontal conduits as abscissa for a specific number of vertical conduits.

The solid lines on the curves show the values of G up to $3V \times 3H$ conduits which is the range covered by ETL tests. These curves then are extrapolated for arrangements up to 6×6 conduits following the general shape of the curves obtained by

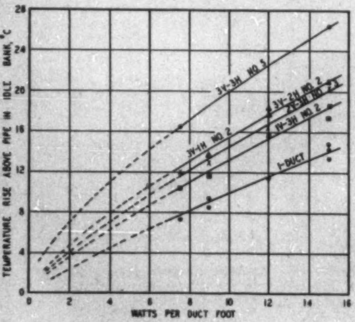


Figure 3 (left). Temperature rise of four-inch black steel conduit

Banks isolated, but not covered

Figure 4 (right). Temperature rise of four-inch black steel conduit

Banks "open"

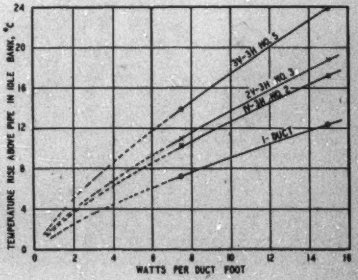


Table IIIB. One-Inch-Conduit Temperature Rise

Duct Arrangement		Temperature Rise = 10 C			Temperature Rise = 20 C			Temperature Rise = 30 C		
Number	Block	Watts Per Foot	A Watts Ratio	B Relative Current	Watts Per Foot	A Watts Ratio	B Relative Current	Watts Per Foot	A Watts Ratio	B Relative Current
One Inch, "Open," 1 and 9 Ducts Only Loaded, Figure 5										
1		3.75	1.000	1.00	8.9	1.000	1.00	14.3	1.000	1.00
9	3*V*-3*H*	1.75	0.467	0.68	4.15	0.446	0.68	7.15	0.500	0.71
One Inch, Covered, 1 and 9 Ducts Only Loaded, Figure 5										
1		3.0	1.000	1.00	6.85	1.000	1.00	11.1	1.000	1.00
9	3*V*-3*H*	1.6	0.533	0.73	3.9	0.569	0.76	6.7	0.604	0.78
One Inch, "Open", Figure 6										
1		3.65	1.000	1.00	8.6	1.000	1.00	14.15	1.000	1.00
3	1*V*-3*H*	2.6	0.712	0.84	6.17	0.718	0.85	10.35	0.730	0.86
6	2*V*-3*H*	2.45	0.644	0.80	5.65	0.657	0.81	9.35	0.661	0.81
6	3*V*-2*H*	2.45	0.644	0.80	5.65	0.657	0.81	9.35	0.661	0.81
3	3*V*-1*H*	2.2	0.603	0.78	5.25	0.610	0.78	8.8	0.622	0.79
9	3*V*-3*H*	1.75	0.479	0.69	4.15	0.482	0.70	7.2	0.509	0.71
One Inch, Covered and Isolated, Figure 7										
1		3.0	1.000	1.00	6.85	1.000	1.00	11.15	1.000	1.00
3	1*V*-3*H*	2.57	0.857	0.93	5.9	0.862	0.93	0.77	0.876	0.94
6	2*V*-3*H*	2.3	0.767	0.88	4.95	0.723	0.85	8.15	0.731	0.86
9	3*V*-3*H*	1.7	0.567	0.75	3.9	0.569	0.75	6.65	0.596	0.77

W. A. Del Mar on an investigation carried out by his company as an aid in interpreting the ETL data. This investigation is summarized in the appendix.

The solid lines in Figure 8 have been drawn straight, as these will fit the points as well as curved lines and give better agreement between the two sets of curves on the drawing. It must be remembered that various points on one set of curves must fall on corresponding points on the other set of curves. For example, the point for $2V \times 3H$ must be the same for both curves. This greatly reduces the possibility of obtaining a better fit of the curves to the points, but also helps in locating the curves where no test values are available. The actual test values are shown in the figure, the points belonging on the various curves being identified by crosses, circles, or triangles at the end of each of the three curves. Designation of the points is shown, so that it can be seen which curve the various points should fall on. The fit of these points does not appear to be good, but when the individual values are considered, the reason for their being off the curve can be seen. For example, the two points above the curve on both sets for three conductors in three rows are values corresponding to conduits without barriers or covers and for barriers with covers. The point through which the curve passes is for the case with barriers but with no covers. This point would seem to be the most logical, and the fact that, both with all barriers removed and with all barriers and covers in place, the results are higher throws some question on the validity of these points.

It may be noted from Table IV that the values for one-inch conduit for different arrangements are close enough to the values for a four-inch conduit, so the same curve may be used for both sizes. Only four-inch values are shown so as not to make the curves confusing.

From these curves it was possible to make up a table of proposed correction factor *G* for various groupings. This correction factor is multiplied by the thermal resistance from the surface of a single conduit, as expressed by *Q*, referred to previously to obtain the correct thermal resistance due to the grouping of this conduit with other similar conduits.

Table V gives the values of *G* which have been obtained from these curves.

The current rating of a cable in a conduit grouped with other conduits is determined by standard methods, using formula 1, for the total thermal resistance, selecting for *G* the proper number and grouping of conduits.

It would be much more convenient if a correction factor could be applied directly to the current rating of a single cable rather than to the thermal resistance, so that standard tables of currents could be used. Such a correction factor may be calculated from the aforementioned values of *G*.

As stated previously, the analysis of the data has been based on a given temperature rise for all groupings of conduits; in other words, the same permissible conductor temperature for the hottest conduit as for a single conduit. This means that the number of watts of heat energy to be dissipated, times the thermal resistance for a single conduit, will be the same as the watts of heat energy of a single conduit as a part of a group, times the thermal resistance of the group, or

$$W_1R_1 = W_gR_g$$

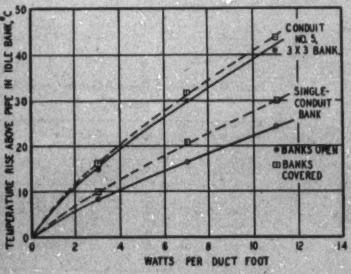
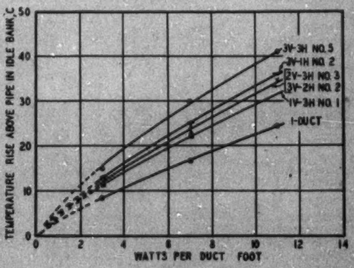


Figure 5 (left). Temperature rise of one-inch black steel conduit

One- and nine-conduit banks only under test

Figure 6 (right). Temperature rise of one-inch black steel conduit

Banks "open"



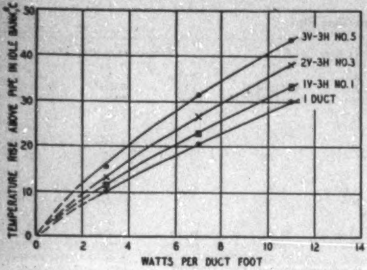


Figure 7 (left). Temperature rise of one-inch black steel conduit

Banks isolated and covered

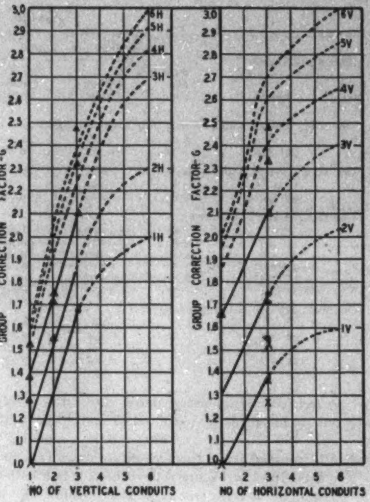


Figure 8. Group correction factor G for various groupings of conduit

×—Test points for single row of conduits

⊙—Test points for two rows of conduits

▲—Test points for three rows of conduits

where 1 signifies a single conduit alone, and g signifies a part of a group of conduits. As

$$W_1=I_1^2r$$

and

$$W_g=I_g^2r$$

Then

$$I_1^2R_1=I_g^2R_g$$

or

$$I_g=I_1\sqrt{R_1/R_g}$$

or

$$I_g=I_1K_c$$

I_1 = current for a cable in a single conduit

I_g = current for a cable in a conduit as a part of a group of conduits

R_1 = thermal resistance of a cable in a single conduit $=R_i+R_s+Q$

R_G = thermal resistance of a cable in a single conduit as a part of a group of conduits $=R_i+R_s+GQ$

$$K_c=\text{correction factor}=\sqrt{\frac{R_i+R_s+Q}{R_i+R_s+GQ}}$$

The square root of the ratio of the thermal resistance of a cable in a single conduit to the thermal resistance of a cable in a conduit as a part of a group of conduits is the correction factor for the currents for a cable in a single conduit to give the same conductor temperature when the cable is in a conduit which is one of a group of conduits. If the value of this correction factor is known, then the current rating of the cable in the group may be calculated directly from the current in the table for a cable in a single conduit by multiplying by this correction factor K.

It is possible to use the correction factor G for conduits also as the correction factor for cables in air, as it applies to the thermal resistance of a dissipating surface. This means that, where there is a group of cables in air, the thermal resistance of a cable becomes

$$R=R_i+GR_s$$

where

R_i = thermal resistance of insulation

R_s = thermal resistance of surface of cable

and G may be taken from Table V.

The correction factor K_a for the current becomes

$$I_g=I_1\sqrt{\frac{R_i+R_s}{R_i+GR_s}}=K_aI_1$$

Table IV. Conduit Group Correction Factor G

Data From ETL Chart

Number of Conduits	One Inch for 10 C Rise: Chart Number	One Inch for 10 C Rise: Watts Per Foot	One Inch for 10 C Rise: G	Four Inches for 15 C Rise: Chart Number	Four Inches for 15 C Rise: Watts Per Foot	Four Inches for 15 C Rise: G
1*V*–1*H* open	36,607	3.66		36,606	19.1	
Barriers open	36,513	3.76		36,605	16.05	
Barriers closed	36,608	3.00		36,464	13.67	
1*V*–3*H* open	36,607	2.60	1.41	36,606	12.5	1.53
Barriers open				36,605	11.65	1.38
Barriers closed	36,608	2.57	1.17	36,464	10.70	1.28
Average			1.29			1.40
2*V*–3*H* open	36,607	2.42	1.51	36,606	11.2	1.71
Barriers open				36,605	10.36	1.55
Barriers closed	36,608	2.20	1.36	36,464	7.80	1.75
Average			1.43			1.67
3*V*–1*H* open						
Barriers open	36,607	2.20	1.71	36,605	9.60	1.67
Barriers closed						
Average			1.71			1.67
3*V*–2*H* open	36,607	2.42	1.51			
Barriers open				36,605	9.60	1.67
Barriers closed						
Average			1.51			1.67
3*V*–3*H* open	36,607	1.73	2.12	36,606	8.25	2.32
Barriers open	36,513	1.75	2.15	36,605	6.50	2.10
Barriers closed	36,608	1.68	1.79	36,464	6.20	2.47
Average			2.02			2.30

Table V. Proposed Conduit Group Correction Factor G

Number of Cables Grouped Vertically	Number of Cables Grouped Horizontally: 1	2	3	4	5	6
1	1.00	1.20	1.40	1.51	1.58	1.60
2	.33	1.54	1.75	1.90	1.99	2.03
3	1.67	1.88	2.10	2.25	2.35	2.40
4	1.85	2.11	2.40	2.52	2.59	2.65
5	1.94	2.24	2.59	2.71	2.79	2.85
6	2.00	2.30	2.70	2.83	2.92	3.06

Table VI. Current-Carrying Capacities of Cables in Air or in Conduit in Air

Number of Conductors in Cable	1	3	1	3	3	1	3	1	3	3	1	3	1	3	3
Kilovolts	0.6	12	0.6	12	0.6	0.6	12	0.6	12	0.6	0.6	12	0.6	12	0.6
Insulation	Varnished Cambric	Paper	Varnished Cambric	Paper	Rubber	Varnished Cambric	Paper	Varnished Cambric	Paper	Rubber	Varnished Cambric	Paper	Varnished Cambric	Paper	Rubber
Conductors in Air or Conduit	Air	Air	Conduit	Conduit	Conduit	Air	Air	Conduit	Conduit	Conduit	Air	Air	Conduit	Conduit	Conduit
Number of Cables in Conduit			3	1	1			3	1	1			3	1	1
Size—American Wire Gauge or Circular Mils	1V-1H					1V-3H					2V-3H				
14	30		23		44	25.5		20.6		40	25		19.8		39
4	133		88			110		80			107		76		
1/0	245	167	158	131		209	143	141	120		197	184	135	118	
4/0	383	247	248	193		326	209	227	178		309	198	216	173	
350,000		336		264			288		240			267		233	
500,000	661	412	404	315	237	558	353	355	288	220	532	333	343	278	214
650,000		480		365			410		334			386		324	
1,000,000	1,001		585			860		520			782		497		

Size—American Wire Gauge or Circular Mils	3V-2H					3V-3H				
14	23.9		19		38	19.4		16.5		33
4	102		74			80		62		
1/0	187	128	129	114		153	105	109	100	
4/0	293	185	204	168		238	154	173	147	
350,000		258		225			209		197	
500,000	506	316	328	271	207	405	262	278	235	183
650,000		375		312			299		271	
1,000,000	774		467			630		406		

where

I_g = current for cable if a group in air

I_1 = current for a single cable in air

$$K_a = \text{correction factor} = \sqrt{\frac{R_i+R_s}{R_i+GR_s}}$$

During the preliminary analysis of the test, values of G were set up prior to those given in Table V, and a study was made of the current rating of a cable in conduit for various conductor sizes. Table VI gives these currents for different cable constructions, and Table VII gives the ratio of the currents for cables in conduits in groups as compared with a single conduit. It is interesting to note that conductor size has very little influence on the ratio, a circumstance which simplifies the problem. With the assumption that conductor size has no influence, Table VIII has been calculated for the correction factor K for three types of insulation of cable. Again, it is fortunate that the kind of insulation has very little influence on the correction factor K, and it is possible to take a value which will be safe for all kinds and slightly more conservative for the others. For example, the paper-insulated cable shows a higher correction factor than for either rubber or varnished-cambric-insulated cables. If the value for rubber cables is taken, then the varnished-cambric and paper cables will be more conservative, but only by a small percentage.

Table IX gives the final proposed values of correction factor K_c for the current rating of a cable in a conduit as a part of a grouping of conduits. The table

Table VII. Reduction Factors for Table VI for Cables in Air or Conduit as Compared With a Single Cable in Air or Conduit

Number of Conductors in Cable	1	3	1	3	3	1	3	1	3	3
Kilovolts	0.6	12	0.6	12	0.6	0.6	12	0.6	12	0.6
Insulation	Varnished Cambric	Paper	Varnished Cambric	Paper	Rubber	Varnished Cambric	Paper	Varnished Cambric	Paper	Rubber
Conductors in Air or Conduit	Air	Air	Conduit	Conduit	Conduit	Air	Air	Conduit	Conduit	Conduit
Number of Cables in Conduit			3	1	1			3	1	1
Size—American Wire Gauge or Circular Mils	1V-3H					2V-3H				
14	0.85		0.91		0.91	0.80		0.89		0.86
4	0.85		0.91			0.80		0.86		
1/0	0.85	0.85	0.89	0.91		0.81	0.80	0.85	0.90	
4/0	0.85	0.84	0.91	0.92		0.80	0.80	0.87	0.89	
350,000		0.85		0.91			0.79		0.88	
500,000	0.84	0.85	0.89	0.91	0.93	0.80	0.81	0.85	0.88	0.87
650,000		0.85		0.91			0.81		0.88	
1,000,000	0.86		0.89			0.78		0.85		

Size—American Wire Gauge or Circular Mils	3V-2H					3V-3H				
14	0.77		0.86		0.89	0.62		0.74		0.75
4	0.76		0.84			0.62		0.70		
1/0	0.77	0.76	0.81	0.89		0.63	0.62	0.69	0.76	
4/0	0.76	0.75	0.82	0.87		0.62	0.62	0.69	0.76	
350,000		0.77		0.85			0.62		0.74	
500,000	0.76	0.77	0.81	0.86	0.90	0.62	0.63	0.69	0.74	0.77
650,000		0.78		0.85			0.62		0.74	
1,000,000	0.77		0.79			0.63		0.69		

Table VIII. Current-Rating Correction

Number of Cables Grouped Vertically	Number of Cables Grouped Horizontally											
	1				2				3			
	a	b	c	d	a	b	c	d	a	b	c	d
In Conduit												
1	1.00	1.00	1.00	1.00	0.94	0.95	0.96	0.94	0.90	0.91	0.93	0.91
2	0.91	0.94	0.94	0.92	0.87	0.88	0.91	0.87	0.83	0.84	0.88	0.84
3	0.84	0.85	0.89	0.85	0.81	0.82	0.86	0.81	0.77	0.78	0.83	0.78
4	0.81	0.82	0.86	0.82	0.77	0.78	0.83	0.78	0.73	0.75	0.80	0.74
5	0.80	0.81	0.85	0.80	0.76	0.77	0.82	0.76	0.71	0.73	0.79	0.72
6	0.79	0.80	0.84	0.79	0.75	0.76	0.81	0.75	0.71	0.72	0.77	0.71
In Air												
1	1.00	1.00	1.00	1.00	0.94	0.93	0.93	0.93	0.89	0.87	0.87	0.87
2	0.91	0.89	0.89	0.89	0.86	0.83	0.83	0.83	0.82	0.79	0.79	0.79
3	0.84	0.80	0.80	0.80	0.80	0.76	0.76	0.76	0.76	0.72	0.72	0.72
4	0.80	0.77	0.77	0.77	0.76	0.72	0.72	0.72	0.72	0.68	0.68	0.68
5	0.79	0.75	0.75	0.75	0.75	0.70	0.70	0.70	0.70	0.66	0.66	0.66
6	0.78	0.74	0.74	0.74	0.74	0.69	0.69	0.69	0.69	0.64	0.64	0.64

a refers to single-conductor 4/0 rubber-insulated cable for five-kilovolt service.

b refers to single-conductor 4/0 varnished-cambric insulated cable for 600-volt service.

c refers to three-conductor 4/0 paper-insulated cable for 12-kv service

d refers to proposed values.

covers groups up to six vertical and six horizontal. The test data did not go beyond 3×3, but the research done by Mr. Del Mar (see Appendix B) gave a means of extrapolating the data. Perhaps undue liberties have been taken, and recommendations should not be made for such a large number of conduits in a group. It is agreed that a total of 36 conduits is not good engineering, yet we know that it is done, and, therefore, we are offering correction factors for these large groupings as a guide to the current loadings that may be used without shortening the life of the cable. In setting up these correction factors, it is assumed that all of the conduits will be loaded with the same kind of cable and 100 per cent load factor. Different load factors for all cables or diversity of loadings for some cables have not been considered. Naturally, a diversity of loading will permit a higher loading, how much, it is difficult to say. As an approximation, it might be taken as the square root of the ratio of the total watts that may be dissipated by all the conduits to the total watts to be dissipated.

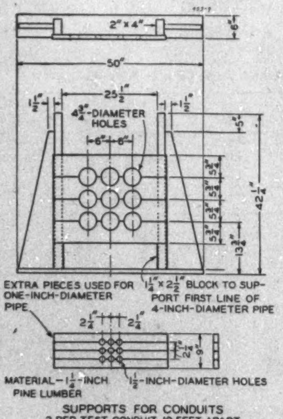

2" x 4"
6"
50"
25 1/2"
1 1/2"
1 1/2"
4 3/4"-DIAMETER HOLES
6"
6"
5"
5 3/4"
5 3/4"
5 3/4"
5 3/4"
42 1/4"
13 3/4"
EXTRA PIECES USED FOR ONE-INCH-DIAMETER PIPE
1 1/4" x 2 1/2" BLOCK TO SUPPORT FIRST LINE OF 4-INCH-DIAMETER PIPE
2 1/4"
2 1/4"
2 1/4"
9"
MATERIAL—1 1/4-INCH PINE LUMBER
1 1/2-INCH-DIAMETER HOLES
SUPPORTS FOR CONDUITS
2 PER TEST CONDUIT 10 FEET APART

Figure 9. Details of conduit supports

Correction factor K_a for air will not be the same as K_c for conduit. As in the case for conduits, these correction factors hold only for cases where the cables are grouped closely together. An increase in spacing results in a decrease in mutual heating effect, and, therefore, a higher value of correction factor applies. These values of correction factor may be taken as giving the maximum reduction in current rating. Tables VI and VII show that, here also, conductor size has very little influence on the correction factor; and Table VIII shows that, as in the case of conduits, the kind of insulation has little influence, the rubber insulation showing less reduction in current rating than varnished-cambric and paper insulation.

Table IX gives the final proposed values for correction factor K_a to determine the current rating of a cable in air as a part of a group of cables in air as compared with a single cable in air.

Table IX. Proposed Current-Rating Correction Factors for Various Groupings of Cables

Number of Cables Grouped Vertically	Number of Cables Grouped Horizontally					
	1	2	3	4	5	6
In Conduit K_c						
1	1.00	0.94	0.91	0.88	0.87	0.86
2	0.92	0.87	0.84	0.81	0.80	0.79
3	0.85	0.81	0.78	0.76	0.75	0.74
4	0.82	0.78	0.74	0.73	0.72	0.72
5	0.80	0.76	0.72	0.71	0.70	0.70
6	0.79	0.75	0.71	0.70	0.69	0.68
In Air K_a						
1	1.00	0.93	0.87	0.84	0.83	0.82
2	0.89	0.83	0.79	0.76	0.75	0.74
3	0.80	0.76	0.72	0.70	0.69	0.68
4	0.77	0.72	0.68	0.67	0.66	0.65
5	0.75	0.70	0.66	0.65	0.64	0.63
6	0.74	0.69	0.64	0.63	0.62	0.61

The IPCEA has adopted these correction factors, for both thermal resistance and current ratings; and they will be found in their most recent tables of current ratings for rubber, varnished-cambric, and paper cables in air and conduit.

Many data were collected on the distribution of temperature around a conduit and also on many of the conduits in a group. Appendix C gives a short summary of this phase of the investigation, and some conclusions are suggested, such as, avoid if possible vertical grouping of conduits, staggering if necessary, and avoid loading up inside conduits in order to get the benefit of the better heat dissipation from outside conduits.

The IPCEA expresses its indebtedness to those manufacturers who contributed the necessary funds to conduct this research. The data obtained have been needed by both users and designers of cable systems, and we are certain that it will have a wide usage contributing to the conservation of materials, now and even after the present emergency has ceased to exist.

Appendix A. Method Used in Determining the Temperature Rise of Conduits for Various Groupings as Reported by ETL

The purpose of these tests was to determine the temperature rise of standard black-enameled rigid-steel conduit of one-inch and four-inch-pipe sizes, supported in still air, over a range of internal watt losses per foot, with various bank arrangements of conduits, and with the banks both enclosed and open.

Factors for Various Groupings of Cables

Number of Cables Grouped Horizontally												
4				5				6				
a	b	c	d	a	b	c	d	a	b	c	d	Number of Cables Grouped Vertically
In Conduit												
0.88	0.88	0.91	0.88	0.87	0.87	0.90	0.87	0.86	0.87	0.90	0.86	1
0.81	0.81	0.86	0.81	0.79	0.80	0.85	0.80	0.78	0.80	0.84	0.79	2
0.75	0.77	0.82	0.76	0.74	0.75	0.81	0.75	0.73	0.75	0.80	0.74	3
0.72	0.74	0.79	0.73	0.71	0.73	0.78	0.72	0.71	0.72	0.78	0.72	4
0.70	0.71	0.77	0.71	0.70	0.70	0.77	0.70	0.69	0.70	0.76	0.70	5
0.69	0.70	0.76	0.70	0.68	0.69	0.76	0.69	0.67	0.68	0.75	0.68	6
In Air												
0.87	0.84	0.84	0.84	0.85	0.83	0.83	0.83	0.85	0.82	0.82	0.82	1
0.80	0.75	0.76	0.76	0.78	0.75	0.75	0.75	0.78	0.74	0.74	0.74	2
0.74	0.70	0.70	0.70	0.73	0.69	0.69	0.69	0.72	0.68	0.68	0.68	3
0.71	0.67	0.67	0.67	0.70	0.66	0.66	0.66	0.70	0.65	0.65	0.65	4
0.69	0.65	0.65	0.65	0.68	0.64	0.64	0.64	0.68	0.63	0.63	0.63	5
0.68	0.63	0.63	0.63	0.67	0.62	0.62	0.62	0.66	0.61	0.61	0.61	6

Method

ARRANGEMENT OF TEST CONDUITS

A total of 560 feet of each of the two sizes was submitted. Two ten-foot lengths coupled together constituted a test conduit. These were set up in six groups or "banks" as follows:

Single duct (1 *V*–1 *H*)
2 vertical and 3 horizontal (2 *V*–3 *H*)
3 vertical and 2 horizontal (3 *V*–2 *H*)
3 vertical and 3 horizontal (3 *V*–3 *H*)
3 vertical and 1 horizontal (3 *V*–1 *H*)
1 vertical and 3 horizontal (1 *V*–3 *H*)

The general method of supporting and spacing the conduits is shown in Figure 9. Two supports were used for the 20-foot test length, one of the supports being located at the mid-point of each of the two coupled lengths of conduit.

The six banks were arranged side by side in a large room and spaced five to six feet center to center over a rectangular area approximately 35 feet on a side. Some tests were made with partitions or barriers of light paper board four feet high separating the bank spaces; others had such barriers, but with these enclosures covered; and still others had no barriers between banks and no covers; that is, all were open to the general room. When the bank spaces were covered, the space above the top conduit or conduits was of the order of two feet.

Figures 10 and 11, respectively, show the layout when entirely open and with one-inch conduits under test, and again when the bank spaces were separated with barriers, and the four-inch conduits were under test.

The test space was near windows along one side of the room. As these faced south, a continuous "curtain" of white cotton muslin was hung up to cut off the sunlight. It was found, however, that a double layer of the cloth was necessary to eliminate completely the effect of direct sunlight.

HEATING OF TEST CONDUITS

The conduits were heated by current circulated through twisted-pair insulated resistance wire of the kind used for soil heating (two number 19 American wire-gauge hot-bed heater wires) supported at the center of each conduit by wood spacers about 30 inches apart. The ends of the conduits were closed with wood stoppers or the approximate equivalent. The twisted-pair heater wires were all cut to exactly the same length; one end of each pair was short-circuited by a movable brass clamp, while the other end was connected to a heavy copper bus extending along one end of the conduit banks. The voltage on this bus was maintained at a constant value. Thus, the watts per foot released were always the same in all 28 conduits, any slight difference that was found initially being reduced to a negligible value by adjusting the short-circuiting clamp at one end. A small switch was connected between one side of the bus and each heater wire. It served two purposes: namely, to permit the simultaneous testing of any number of conduits, and to facilitate the convenient insertion of an ammeter or wattmeter in each heater circuit.

Power was obtained from the 60-candle-power public-utility supply through a booster generator controlled by a thermionic regulator, an arrangement which provides a source of electric power at an extremely steady voltage. Adjustment of the loads was obtained by means of an induction regulator.

MEASUREMENT OF TEMPERATURE

The temperatures of the conduits were measured by means of copper-advance thermocouples (wire-size number 26 American

Figure 10. One-inch-conduit banks under test

No barriers or covers

Table X. Summary of Final Rises, Run 26, Four-Inch Conduit

IV-3H, 3V-3H, 2V-3H and Single-Duct Banks, Covered and With Barriers—Load on at 15 Watts Per Foot at 9 A. M.

	Degrees Centigrade			
	1 Duct	(3) 2V–3H	(35) V–3H	(2) 1V–3H
Average pipe temperature	30.3	47.2	50.2	42.2
Average ambient temperature	24.2	27.1	28.9	26.1
Average room temperature	22.4	22.4	22.4	22.4
Average idle-duct and reference-pipe temperature	23.2	23.2	23.2	23.2
Rise above ambient temperature	15.1	20.1	21.3	16.1
Rise above room temperature	16.9	24.8	27.8	19.8
Rise above idle-duct and reference-pipe temperature	16.1	24.0	27.0	19.0

Figure 11. Barriers between conduit banks in place (without covers)

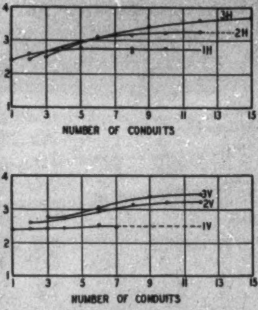


Figure 12. Thermal resistance of one-inch conduits (Habirshaw test data)

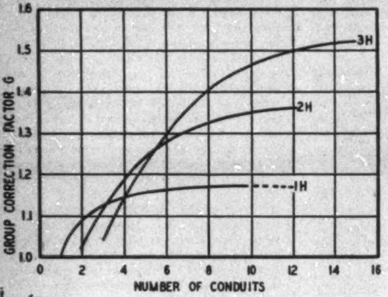
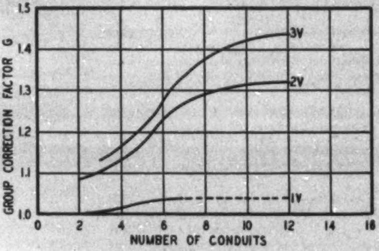


Figure 13 (left and right). Group correction factor G (from Habirshaw test data)



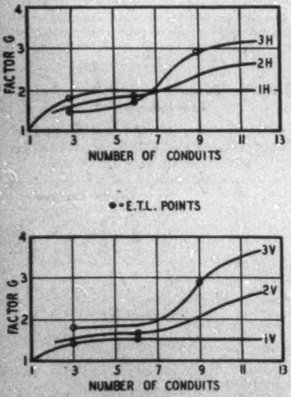


Figure 14. Correction factor G determined from ETL tests on four-inch conduit extrapolated by use of Habirshaw test data on one-inch conduit

Figure 15 (right). Thermal resistance for one-inch conduit from ETL and Habirshaw tests

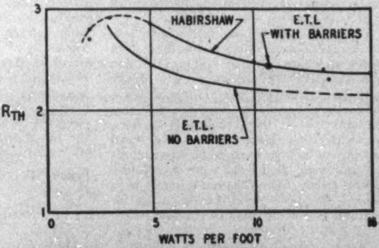


Figure 16. One-inch conduit under test (Habirshaw)

Front opened to afford view of conduit

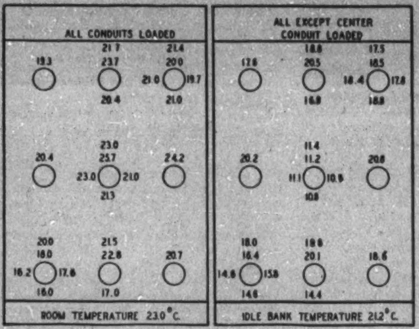

ALL CONDUITS LOADED
21.7
21.4
19.3
23.7
20.0
21.0
19.7
20.4
21.0
23.0
20.4
25.7
24.2
23.0
21.0
21.3
20.0
21.5
18.0
22.8
20.7
16.2
17.6
16.0
17.0
ROOM TEMPERATURE 23.0°C.
ALL EXCEPT CENTER CONDUIT LOADED
18.8
17.5
17.6
20.5
18.5
18.4
17.8
16.8
18.8
11.4
20.2
11.2
20.8
11.1
10.5
10.8
18.0
19.8
16.4
20.1
18.6
14.8
15.8
14.6
14.4
IDLE BANK TEMPERATURE 21.2°C.

Figure 17 (left). Temperature rise for nine-conduit banks of four-inch conduit at 15 watts per foot

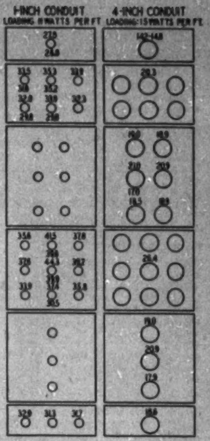

1-INCH CONDUIT
LOADING 11 WATTS PER FT
4-INCH CONDUIT
LOADING 15 WATTS PER FT

Figure 19. Temperature rise above idle conduit with barriers, but not covered

wire gauge) securely attached to the outer surface. Sufficient couples were used to show the approximate differences between the temperatures at various locations in the conduit bank, but the great majority, in the case of the four-inch conduit, and all, in the case of the one-inch conduit, were attached to the top surface of the pipe. In the four-inch conduit tests 97 couples were installed. In the one-inch conduit tests 73 couples were installed. All couples were connected to suitable selector switches on the test table, shown in Figures 9 and 10, which permitted rapid transfer of the potentiometer-type temperature indicator (Leeds and Northrup, portable form) from one couple to another.

The suitability of the method employed to attach the couple junction to the surface of the pipe was demonstrated by embedding in the pipe wall the junction of an extra couple adjacent to a surface couple. Repeated measurements showed agreement between the two couples within a maximum of about one degree centigrade at the highest temperatures attained, the embedded couple showing the higher temperature.

The reference temperatures observed are:

(*a*). The *ambient temperature*, that is, the temperature of the air adjacent to the conduits of a particular conduit bank.

(*b*). *Idle-conduit temperature*, that is, temperature of one or more conduits carrying no "load," including a piece of four-inch wrought-iron water pipe about six-feet long which was used to represent an idle conduit when the test conduits were all loaded.

(*c*). The *temperature of the open room* adjacent to the space occupied by the six conduit banks, measured on the east and west sides, respectively.

The ambient or immediately adjacent air temperature in each duct space was taken

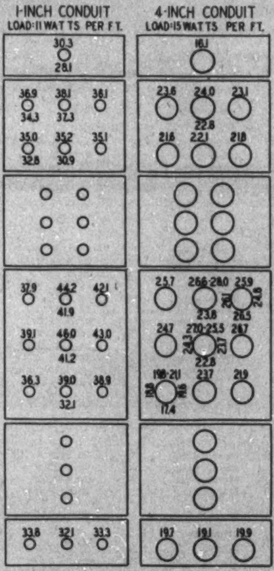

1-INCH CONDUIT
LOAD: 11 WATTS PER FT.
4-INCH CONDUIT
LOAD: 15 WATTS PER FT.
30.3
28.1
18.1
36.9
38.1
36.1
34.3
37.3
35.0
35.2
35.1
32.8
30.9
23.6
24.0
23.1
22.8
21.6
22.1
21.8
37.9
44.2
42.1
41.9
39.1
46.0
43.0
41.2
36.3
39.0
38.9
32.1
25.7
26.6-28.0
25.9
23.6
24.7
27.0-25.5
24.7
22.8
19.8-21.1
23.7
21.9
17.4
33.8
32.1
33.3
19.7
19.1
19.9

Figure 18. Temperature rise above idle conduit with barriers and covers

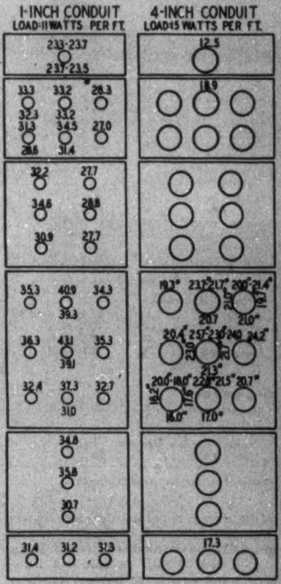

1-INCH CONDUIT
LOAD: 11 WATTS PER FT
4-INCH CONDUIT
LOAD: 15 WATTS PER FT.
23.3-23.7
23.7-23.5
12.5
33.3
33.2
28.3
32.3
33.2
31.3
34.5
27.0
28.6
31.4
18.9
32.2
27.7
34.6
28.8
30.9
27.7
35.3
40.9
34.3
39.3
36.3
43.1
35.3
39.1
32.4
37.3
32.7
31.0
34.8
35.8
30.7
31.4
31.2
31.3
17.3

Figure 20. Temperature rise above idle conduit

Banks "open." Figures shown with an asterisk were obtained in the first series of tests at the higher loads and were not repeated. Rises in this case are above room and not above idle conduit

Table XI. Spot Readings

Selector-Switch Position	Couple	Degrees Centigrade			
		12:00 M.	1:00 P.M.	2:00 P.M.	3:15 P.M.
486-1	2	39.5	39.0–39.0	39.9	39.8
486-9	10	43.5	44.7	45.5	45.6
492-2	20	22.0	23.0–23.0	23.2	23.5
8*D*21-1	28	48.4	50.0	50.1	50.3
Mercury cup	71	44.3	45.0–45.3	45.8–45.7	46.0–45.7
8*D*22-1	39	24.1	25.6–25.7	26.3	26.5
8*D*22-4	42	40.9	41.7	42.2	42.6
8*D*22-6	96	21.0	21.3	21.9	22.0
Mercury cup	92	24.1	24.1–24.1	25.2–25.3	23.9–24.0–23.8
Mercury cup	X	22.0	22.1	22.9	23.1

Table XII. Run 26

Final Readings at 3:45 P.M.

Selector-Switch Position	Couple	Temperature (Degrees Centigrade)	Selector-Switch Position	Couple	Temperature (Degrees Centigrade)
Single Duct					
486-0	1	39.0–39.1–39.1–38.8	Mercury cup	44	24.2–24.2
486-1	2	39.7–39.7	Mercury cup	45	24.1
486-2	3	39.6	Mercury cup	81	23.9–23.9
486-3	4	39.1	Mercury cup	80	24.5–24.6
486-4	5	40.1–39.9			
486-5	6	39.0–39.0	Average		24.2
Average		39.3			
2 Vertical, 3 Horizontal					
486-6	7	46.9–46.7	Mercury cup	46	26.9
486-7	8	44.7–44.7–44.9	Mercury cup	47	27.9–27.9
486-8*	9	47.3–47.2	Mercury cup	83	27.0–26.9
Mercury cup	89	45.9–46.0	Mercury cup	82	26.0
486-9	10	45.3–45.3			
486-10	11	46.4–46.3	Average		27.1
486-11	12	45.0			
486-12	13	47.0			
486-13	14	47.0			
486-14	15	46.2			
486-15	16	47.4			
496-16	17	47.1–47.0			
2 Vertical, 2 Horizontal (Idle Bank)					
492-2	20	23.0–23.1	Mercury cup	85	24.0–24.0
492-5	23	23.0–23.1	Mercury cup	84	23.7–23.7
Average		23.0	Average		23.8
3 Vertical, 3 Horizontal					
492-6	24	48.9–49.0	Mercury cup	66	49.8–49.9
492-7	25	47.9	Mercury cup	67	47.1–46.9
492-8	26	42.9–43.0	Mercury cup	68	48.6–48.8
8*D*21-0	27	51.2–51.2	Mercury cup	69	47.5
8*D*21-1	28	50.2	Mercury cup	70	46.9–46.9
8*D*21-2	29	46.9–46.9	Mercury cup	71	46.0
8*D*21-3	30	49.1			
8*D*21-4	31	49.9–49.9			
8*D*21-5	32	45.1			
8*D*21-6	33	51.9–52.0	Mercury cup	72	31.3–31.2
8*D*21-7	34	53.0–52.9	Mercury cup	73	26.5–26.0
8*D*21-8	35	52.4	Mercury cup	74	30.9
8*D*21-9	36	51.7–51.9	Mercury cup	75	26.2–26.2
8*D*21-10	37	51.5–51.7	Mercury cup	76	31.9
Average, conduit 5* (33 to 37 inclusive plus 68 to 71 inclusive)—50.2			Mercury cup	77	27.7–27.5
			Mercury cup	78	32.0–32.0
Mercury cup	56	49.1	Mercury cup	79	26.9–27.0
Mercury cup	57	49.3	Mercury cup	50	30.0–30.2–30.0
Mercury cup	58	47.9–48.0	Mercury cup	51	32.2–32.2
Mercury cup	59	49.7–49.7	Average	72 to 79 inclusive plus 50 and 51—28.9	
Mercury cup	60	40.6–40.5			
Mercury cup	61	41.9–42.0			
Mercury cup	62	42.9–42.7			
Mercury cup	63	44.3–44.3			
1 Vertical, 3 Horizontal					
8*D*22-3	41	42.8–42.8–42.9	Mercury cup	54	25.9–26.1
8*D*22-4*	42	42.2–42.3	Mercury cup	55	26.0–25.9
8*D*22-5	43	43.2–43.0	Mercury cup	93	26.4–26.3
Mercury cup	95	42.0–41.9	Mercury cup	94	25.8–26.0
			Average		26.1
Reference Pipe			**Room Air**		
Mercury cup	X	23.4–23.0	8*D*22-6	96	21.8–21.6
			Mercury cup	92	23.9
Average reference pipe and idle conduit		23.1	Average		22.4

* Selected as "hottest" conduit of bank.

Table XIII. Conduit Correction Factor Q

Nominal Conduit Diameter (Inches)	Conduit Correction Factor Q—100 Per Cent Load Factor		
	Single- or Multiple-Conductor Cables Per Conduit		
	One	Two	Three
0.50	3.9	9.5	15.4
0.75	3.6	8.6	13.9
1.00	3.2	7.6	12.4
1.25	2.8	6.7	11.0
1.50	2.6	6.2	10.2
2.00	2.3	5.5	8.9
2.50	2.1	4.8	7.6
3.00	1.9	4.2	6.6
3.50	1.8	3.8	6.0
4.00	1.7	3.5	5.5
4.50	1.6	3.3	5.2

These values refer to number of cables in a conduit, not to number of conductors in a conduit. Example: The correction factor for one three-conductor cable in a two-inch conduit is 2.3, whereas, for three single-conductor cables in same size conduit, it is 8.9.

Tables do not extend beyond 4.5-inch nominal outside diameter because of unknown losses when very large conductor sizes are used.

Table XIV

Vertical Groups		Horizontal Groups	
High	Wide	Wide	High
1	1, 2, 3	1	1, 2, 3
2	1, 2, 3	2	1, 2, 3
3	1, 2, 3	3	1, 2, 3
4	1, 2, 3	4	1, 2, 3
5	1, 2, 3	5	1, 2
6	1, 2	6	1, 2
7	1	7	1, 2
8	1		
10	1		

as the average of not less than four couples suspended in the air at the four corners formed by the two conduit supports. In the case of the $3V \times 3H$ or nine-duct bank, there was a total of ten ambient couples in the air around the bank, suspended about 15 inches from the nearest conduit of the bank, and from about 15 to 25 inches above the floor.

Test Conditions

As indicated previously, the heating current was supplied with voltage automatically regulated to be practically constant; the variation in the watts supplied to the conduits rarely exceeded the limits of ±0.25 per cent of the value to which the load had been adjusted.

In each test run, the test was continued until readings of the indications of representative couples showed constant temperature rises for at least one hour. This usually required from five to six hours of heating.

In general, the procedure was to make a "run" at each of a sufficient number of currents to give a reliable curve between load (watts per foot of conduit) and temperature rise, after which values for any load condition could be found (see Table X).

Appendix B. Supplemental Work in Habirshaw Laboratories

When the ETL values of G in Table IV are plotted against the number of conduits in a group, the points are very sparse and appear to be somewhat erratic, making it difficult to draw curves through them with any degree of reliability.

The work in the Habirshaw Laboratories was initiated for the purpose of finding the shape of the curves of R_{th} and G and not to check the ETL values. By this means it was hoped that curves might be drawn which, by interpolation and extrapolation, would yield reasonably accurate data over a wider range from the limited number of cases covered by the ETL tests.

These tests were made with one-inch pipe operated at temperatures considerably above those likely to be encountered in service in order to obtain fair accuracy in spite of variations of ambient temperature and unavoidable drafts.

Tests were made on groups, as shown in Table XIV.

The results are shown in Figures 12 and 13, while Figure 14 shows curves of similar shape drawn through the ETL points, thus justifying the apparently erratic ETL data by the unexpected crossing of the curves.

In addition to this work, the relation between R_{th} and watts dissipated was determined for a single conduit, a plot of which is shown in Figure 15. The ETL data fit this curve, thus affording a point of contact between the ETL and Habirshaw data, thereby indicating that the two sets of tests are consistent and mutually complementary.

The setup for the tests is shown in Figure 16, the front opened to afford a view of the pipes.

Thermocouples at the centers of the pipes gave the basic temperature data, but tests were made with pipes equipped with several thermocouples which showed a practically uniform temperature for the part of the pipe within the enclosure.

Appendix C

Appendix A, under description of test procedure, refers to the large number of thermocouples which were attached to the conduits and which permitted taking a temperature survey of the various conduits. The following brief description of the results may be of interest.

Figure 17 (ETL plate 36,609) shows the temperature rise for the nine conduits when all nine conduits carried the same load of 15 watts per foot, and when the center conduit (5) did not carry load. This test was made to show the marked reduction in the temperature of the center conduit if not carrying load, but only affected by the surrounding conduits. As shown in Figure 12, the room air temperature was reported for all conduits loaded and for the idle bank temperature for eight conduits loaded. The room air temperature for the latter case was 20.9 degrees centigrade; therefore, to get a more accurate comparison of temperature rises, the values given for eight conduits loaded should be increased by 0.3 degree centigrade. The pocketing of heat around the center conduit, when loaded, is clearly shown. There is a reduction of ten degrees centigrade on the bottom of the center conduit and 14 degrees centigrade at the top of the center conduit when it does not carry load. It is also interesting to note that even the outside conduits show a lower temperature when the center conduit does not carry load.

The temperature rises given in Tables I and II are based on the temperatures measured on the top side of the conduits, that is, the hottest point. In a few cases the temperatures were measured on one or more of the other sides in addition to the top side. The corresponding rises above the temperature of idle conduits, including the top side reported in the tables, are shown in the following diagrams for one load for each size of conduit and for the three exposures:

Figure 18 (ETL plate 36,610)—with barriers and covered.

Figure 19 (ETL plate 36,611)—with barriers but not covered.

Figure 20 (ETL plate 36,612)—banks open, no barriers, no covers.

The temperature rises noted in Figures 18, 19, and 20 for the two sizes of conduit cannot be compared, because the loads are higher than would be carried in service. We would not expect temperature rises of more than ten degrees centigrade for the one-inch conduit and 15 degrees centigrade for the four-inch conduit. They do show which conduit will be hottest, and how grouping does give higher temperatures, as the number of conduits increases. In the case of three conduits all in the same horizontal plane, there should not be much difference between conduits. It is surprising to find the center conduit showing a slightly lower temperature than the other two. When all three conduits are in a vertical plane, the center conduit is hottest, as is to be expected. The grouping of six conduits, two vertical and three horizontal, show the center conduit of top row the hottest—again to be expected. Where the grouping is three vertical and two horizontal, the center conduits are the hottest, as expected. As in Figure 12, the center conduit of a group of nine conduits, three vertical and three horizontal, shows the highest temperature rise.

Although the data shown are meager, they indicate that a vertical grouping of conduits should be avoided whenever possible. A staggering of the conduits vertically, so that there is a chance to produce convection-air currents around the conduits, will help to overcome the impinging of the heat of lower conduits on those directly above.

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Transformer Magnetizing Inrush Currents and Influence on System Operation

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WHEN a transformer is energized, a transient current, known as magnetizing inrush current, generally flows for a short period of time until normal flux conditions are established. Under most practical system conditions, this current transient is of little consequence. However, in very rare cases a combination of circumstances may be obtained which results in this inrush being of such consequence as to impair momentarily the proper operation of the system. Because of the numerous factors bearing upon this general problem, an investigation has been made to determine the effects of transformer inrush currents under a wide variety of system conditions.

That transformers may require a large initial transient magnetizing current when connected to a voltage source has long been recognized.[1-3] Explanations of the nature of this initial inrush current, together with oscillograms obtained from tests on a few specific transformers, have been published. No information, however, has been found in the technical literature defining the parameters which determine the variations in the transient inrush current among individual transformers, or indicating how the inrush current may be affected by the design and connections of the transformers themselves or by the characteristics of the electric systems to which they are applied.

A working knowledge of these factors is made increasingly important by advances in the art of transformer design and manufacture. A particularly important advance is the trend toward the use of improved silicon steel. The use of this steel offers several distinct advantages, namely:

1. Permits operation at higher flux densities without increasing core loss, exciting current, or core temperature.

2. Permits a saving of steel and copper, both critical materials, in that less actual material is required for a given design.

3. Reduces the size and weight of transformers per kilovolt-ampere capacity.

The attainment of these advantages, however, has been accompanied by a tendency to increase the initial transient inrush current. This current, together with the system voltage dips which it may produce, can have an important bearing not only on the application of the transformers themselves, but also on fuses, relays, contactors, motors, or other system equipment which is influenced by the inrush current or the resulting system voltage dips.

It is the purpose of this paper to discuss the mechanism by which inrush currents are produced, the results of tests and calculations, and studies made with the miniature-system analyzer. Factors that determine the significance of inrush current from the standpoint of system operation and methods for reducing the inrush current or mitigating its effects are also discussed.

Effects of Inrush Current on System Equipment

The magnetizing inrush currents of transformers may, in extreme cases, approach in magnitude the currents which result from short circuits across their secondary terminals. Calculations and measurements indicate that, on typical systems, inrush currents may cause the voltage level of one phase at the transformer terminals to drop as much as 20 per cent momentarily. Fortunately, however, even these momentary currents and voltage dips occur only very infrequently, as transformers generally are not switched often; and, when they are energized, only the pyramiding of several critical factors, which are discussed later, can produce disturbances as severe as those obtainable under carefully controlled test conditions. In this paper, the emphasis is placed on the extreme values of inrush current and the resulting system disturbances, because these define the upper limit of the problem. However, in many actual cases, the practical effects of inrush will be greatly reduced and may even be absent altogether.

The effects of inrush currents and voltage dips on other system equipment are most pronounced in the case of fuses and some types of relays but may also influence the behavior of motors, starting contactors, and lighting circuits.

In applying fuses in series with transformers, care should be taken to select fuses which are large enough so that they will not be damaged by the transformer inrush current, but on the other hand the fuses selected should not be unnecessarily large so that inadequate protection to the transformers and system results. Although it is often the practice to apply fuses on the basis that their normal current ratings should be approximately equal to some multiple of the transformer full-load current, this occasionally results in unnecessary fuse blowing because the normal fuse rating does not in any way reflect its characteristics in the short-time–high-current part of the range. For example, current-limiting fuses inherently have limited ability to withstand current surges, while other fuses are available that have specially designed characteristics to permit them to withstand relatively large overcurrents for short periods. Hence, since inrush phenomena are usually associated with the first few seconds or less after transformers are energized, this is the part of the fuse-characteristic curve that should be examined, and the fuse applied on this basis.

A series of tests, to which later reference is made, has indicated that there is a simple criterion for selecting fuses which will be adequate to withstand the inrush current, namely, that the fuses selected should have such characteristics that they will carry without damage 12 times the normal transformer full-load current for one tenth of a second.

Where transformers are expected to be loaded above their nameplate kilovolt-ampere ratings in accordance with published guides for transformer operation[4,5]

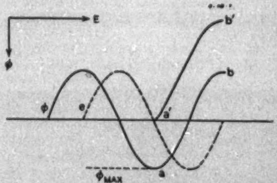

Figure 1. Flux and voltage relation at instant of switching

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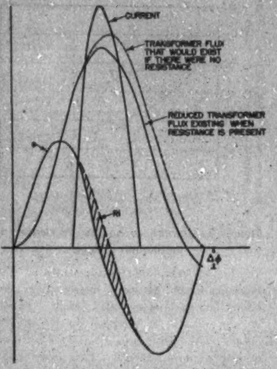


Figure 2. Effect of circuit resistance during first cycle after switching in at instant of voltage zero

the fuses selected must necessarily be of sufficient rating to meet the increased load-carrying requirements, and will, therefore, almost certainly be large enough so that there is no possibility of being damaged by inrush current. The fusing of potential transformers constitutes a special problem and has been covered elsewhere.[6] It has been found also that, where fused transformers are exposed to the effects of lightning surges, the fuse ratings must be greater than a certain minimum value to avoid being damaged by the lightning current.[7]

Nearly all large or important power transformers are protected in event of internal faults by percentage differential relays and circuit breakers which operate to isolate the transformers from all sources of power if the currents in their windings differ by more than a predetermined per cent from one another. Since magnetizing inrush currents flow in the transformer primary windings only, the differential relays may respond as for transformer internal faults, thereby tripping the breakers immediately after they have been closed. Although the relays used for transformer applications are made inherently less sensitive than those used for differential protection of busses or generators, in recognition of the inrush problem, this is not always sufficient to prevent false operation. A remedy sometimes used to prevent such false operation consists in temporarily reducing further the sensitivity of the differential relays or making them entirely ineffective for a few seconds or until the inrush current has had an opportunity to decay.[8] Unfortunately, however, this tends in some measure to defeat the purpose of the differential relays, since insulation failures in transformers have been known to occur at the instant of energizing, particularly in the case of transformers that have been standing idle for long periods of time

Harmonic-restrained differential relays offer another solution to the problem.[9] In these relays the restraining forces are produced by harmonic components of current; and since typical inrush-current wave forms are rich in harmonic content, relay operation is prevented so long as the differential current is of this type. Full sensitivity of operation, however, is retained for protecting the transformers if faults should develop during the transient inrush period. For parallel operation of transformers, one harmonic-restraint relay must be used with each transformer bank in order to insure correct relay operation.[10]

Induction-type inverse-time overcurrent relays, as normally used for overload and short-circuit protection of power transformers, are not likely to be caused to operate by magnetizing inrush currents, except possibly when used for protecting transformers having unusually severe inrush-current requirements, and then only if the relays are adjusted for minimum pickup and fastest time-lever setting. Instantaneous plunger-type overcurrent relays similarly used occasionally might be caused to operate by the inrush current.

With correctly applied motors and starting equipment, difficulties resulting from the system voltage dips produced by transformer inrush current are not likely to occur. The starting contactors normally used with small and medium-size motors are held in their closed position by magnet coils connected across the source of supply voltage. These contactors are designed to remain closed until the voltage falls to 60 per cent or less of rated value and hence are unaffected by the voltage dips resulting from transformer magnetizing inrush currents, since the system voltage during the inrush period usually does not drop to this low level even momentarily. Large motors are usually provided with undervoltage protective relays which may be adjusted to cause the motors to be disconnected from the system at any predetermined drop in voltage. These relays, however, are normally of the time-delay type and therefore are not caused to operate by the transient voltage dips produced by connecting transformers to the system. However, the motors themselves, may, if heavily loaded, tend to pull out of step or to stall when the system voltage drops materially. With correctly applied motors, troubles of this nature are not likely to occur unless the transformer inrush current is sufficient to lower the voltage at the motor terminals approximately 20 per cent, which, as already stated, is about the maximum voltage drop that may be expected to result from this cause.

The switching of transformers of appreciable size is seldom done directly on low-voltage lighting circuits, and hence light flicker from this cause is rarely a problem. However, if transformers are switched at a substation which supplies lighting circuits, this may cause lamp flicker over the entire area served by the substation. This will be particularly true in the case of single-phase circuits or

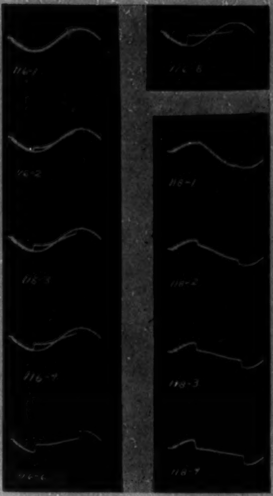


Figure 3. Effect of source reactance on voltage wave shape

Single-phase miniature-system transformer 101.5 volts at source, base ohms = 450 ohms, 3 per cent exciting current. Effect of system impedance on inrush current, and voltage wave shape. Transformer d-c resistance, 5.57 ohms. Saturation curve *A* of Figure 12

System Reactance (Per Cent)	Times Normal Crest Current Inrush	Voltage Across Transformer	
		Primary	Secondary
0	26.5	116-1	118-1
1.18	21.0	116-2	
2.36	18.0	116-3	118-2
2.93	17.0	116-4	
5.86	11.5	116-6	118-3
11.4	7.2	116-8	118-4

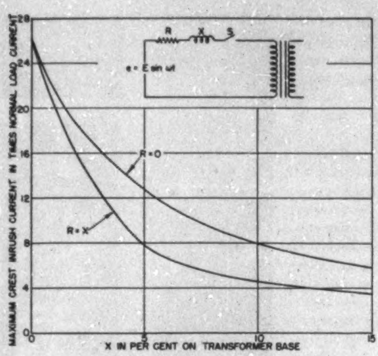


Figure 4. Effect of source impedance on magnitude of inrush currents

Residual of magnitude and polarity to give maximum inrush current. Instant of closing controlled to give maximum inrush. Source voltage normal as defined by Figure 12. Single-phase transformer, assuming 3 per cent normal exciting current. Base ohms = 450 ohms. Saturation curve *A* of Figure 12

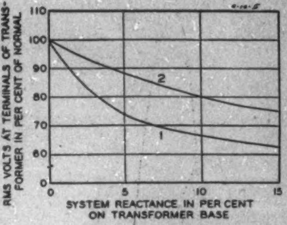


Figure 5. Effect of source reactance on system rms voltage

Residual of magnitude and polarity to give maximum inrush current. Instant of closing controlled to give maximum inrush. Source voltage normal as defined by Figure 12. Single-phase transformer, assuming 3 per cent normal exciting current. Base ohms = 450 ohms. Saturation curve *A* of Figure 12

1. Curve *A* of Figure 12
2. Curve *B* of Figure 12

certain three-phase circuits (for example, four-wire with wye-connected transformers) where the inrush associated with any one phase is not modified by the presence of the other phases. As stated previously, however, the switching of transformers is generally so infrequent an occurrence that the resulting voltage dips are not a serious problem. On the other hand, if the switching is done frequently, the light flicker resulting even from small voltage dips may become objectionable through repetition.

Inrush-Current Phenomena

It is well known that when a transformer is connected to a circuit, the current taken by it generally will not assume immediately its final or normal wave form and magnitude.

The magnitude, as well as the number of cycles of the transient, depends on:

1. The point of the voltage wave at the moment of the closing of the switch.
2. The value of residual magnetism and its sign with respect to the first half cycle of the alternating flux.
3. Saturation or maximum flux-carrying capacity of the iron.
4. Total impedance of the circuit through which the current flows.

PHYSICAL EXPLANATION

Under normal steady-state conditions corresponding to a voltage wave *E*, a flux wave ϕ in quadrature with the voltage exists ignoring the negligible resistance drop of the primary winding for exciting current. Starting from *a*, Figure 1, corresponding to the negative maximum point of the flux wave and the zero point of the voltage wave, the flux at *a* has a value $(-\phi_{max})$ and, in one-half cycle, increases through zero and reaches a maximum value of $(+\phi_{max})$ at *b*. So

$$\phi_{max}-(-\phi_{max})=2\phi_{max}$$

is the change in flux necessary to produce the electromotive force *E*.

It follows from this elementary consideration that if the transformer is energized by closing the primary switch at the instant *a* corresponding to the zero point of the voltage wave, and, if it is presumed that there is no residual flux, the maximum peak flux corresponding to the impressed voltage *E* will become $2\phi_{max}$ one-half cycle after the closing of the switch. The flux curve is shown in Figure 1, *a′b′*.

If the core at the instant of closing the switch possessed a residual flux (ϕ_{res}) the final maximum peak flux becomes

$$2\phi_{max}\pm\phi_{res}$$

From this it is evident that residual may increase or decrease the maximum peak value of the flux.

Not all of the flux is carried by the core. The core can carry only a flux (ϕ_{sat}) corresponding to its saturation; the rest is carried by space. The flux that is carried by space and has its return path through the air is:

$$2\phi_{max}+\phi_{res}-\phi_{sat}=\phi_1 \tag{1}$$

The current to produce this flux is

$$I_{max\ exc}=\frac{\phi_1}{L_1} \tag{2}$$

where L_1 is the air-core inductance of the winding under consideration.

Theoretically the current wave corresponding to the flux wave can be calculated with the aid of the hysteresis loop (or average magnetization) curve of the core. But hysteresis loops or magnetization curves cannot be obtained very easily for very high densities and generally are not available. The peak

Table I. Relative Magnetizing Inrush Currents for Various Transformer Connections

Transformer Connection		Three Single-Phase Transformers		Three-Phase Transformer	
Primary	Secondary	Simultaneous Switching	Sequential Switching	Simultaneous Switching	Sequential Switching
Y⏚	▷ OR Y	26 × normal	26 × normal	13 × normal	14.5 × normal
Y⏚	▷	26 × normal	29 × normal	13 × normal	14.5 × normal
Y	▷ OR Y	20 × normal	20 × normal	11.0 × normal	11.0 × normal
Y	▷	20 × normal	20 × normal	11.0 × normal	11.0 × normal
▷	▷ OR Y	20* × normal	30† × normal	15.5‡ × normal	15.5‡ × normal
▷	▷	20* × normal	30† × normal	15.5‡ × normal	15.5‡ × normal

* Based on normal phase current; equals 11.5 based on normal line current.
† Based on normal phase current; equals 17.3 based on normal line current.
‡ Based on normal phase current; equals 9.0 based on normal line current.

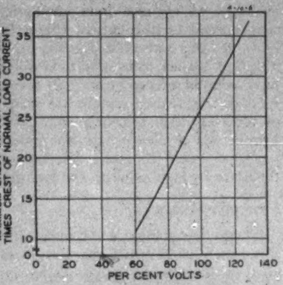
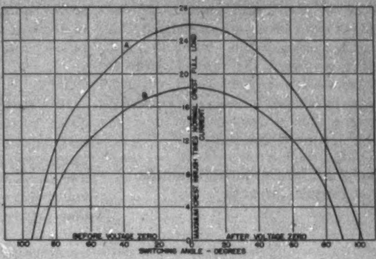


Figure 6. Effect of system voltage on maximum crest inrush current

Residual of magnitude and of polarity to give maximum inrush. Closing at voltage zero. Source voltage normal as defined by Figure 12. Single-phase transformer, assuming 3 per cent normal exciting current. Base ohms = 450 ohms



Curve *A*—Residual of magnitude and polarity to give maximum inrush. Source voltage normal as defined by Figure 12. Single-phase transformer, assuming three per cent normal exciting current. Base ohms = 450 ohms

Curve *B*—No residual

Figure 7. Maximum crest inrush current versus switching angle

value of the current, however, can be approximated fairly closely as follows:

With reference to equations 1 and 2 the maximum flux density which iron molecules can add to a given magnetic field is estimated as 130 kilolines per square inch. At densities greater than this value the field behaves as if the medium were entirely air, with no iron present.

In the present case, the maximum peak flux is equal to $(B_{res}+2B_{max})\ A_c$, A_c being the cross-sectional area of the core. Of this, then, something like 130 A_c kilolines will be carried by the iron, the rest $(B_{res}+2B_{max}-130)\ A_s$ by space A_s which is the space occupied by the core legs plus the space between the core legs and the excited winding.

The ampere-turns necessary to produce $(B_{res}+2B_{max}-130)\ A_c/A_s$ kilolines density in space will ordinarily be so large that it may be considered as adequate to produce the 130 kilolines density in the iron molecules in parallel with the flux in space; and, therefore, the inrush current for the saturated range of the wave can be calculated in terms of the air-core inductance of the winding and the space flux.

Briefly, if B_s is the flux density in space,

$$B_s=\frac{A_c}{A_s}(B_{res}+2B_{max}-130) \text{ kilolines per square inch} \quad (3)$$

and, therefore, if the length of the solenoid is h inches, and the turns in series n, the maximum instantaneous current i_{max} will be

$$i_{max}=\frac{1{,}000hB_s}{3.2n}=\frac{10^3hA_c(B_{res}+2B_{max}-130)}{3.2nA_s} \quad (4)$$

This formula is only approximate and is offered not for the calculation of the entire wave but only for its peak.[1]

Since, as explained heretofore, the inrush peak is determined by the air-core inductance of the excited winding and since generally the higher-voltage winding has higher percentage inductance, therefore, exciting a transformer through its higher-voltage winding leads to smaller inrush current. This is particularly true of concentric transformers in which the higher voltage is generally the outer solenoid, the lower-voltage winding the inner.

TIME OF SWITCHING AND RATE OF DECAY OF THE INRUSH CURRENT

The flux wave, *a′b′* of Figure 1 is sinusoidal and offset. It can be resolved into two components: a sinusoidal a-c component and a d-c component which eventually decays. The general equation for the transformer flux can be expressed as follows:

$$\phi=(\phi_R+\phi_{max}\cos\theta)-R\int_0^t i\,dt-\phi_{max}\cos(\omega t+\theta) \quad (5)$$

where

ϕ_R = residual flux at the instant immediately preceding energizing the transformer

ϕ_{max} = maximum value of steady-state flux

R = resistance of circuit through which magnetizing current flows

ϕ = instantaneous value of flux at any time

θ = the phase angle measured from the instant of zero voltage at which the transformer is energized

Since the flux controls the inrush current from the preceding expression, it will be seen that the maximum crest inrush current will be obtained when $\theta=0, \pi, 2\pi\ \ldots$ which leads to the well-known conclusion that closing the switch at a voltage zero results in the maximum inrush. It should be noted, however, that the d-c component is not reduced greatly by a small variation from a voltage zero. In fact, assuming no residual flux, if $\theta=30$ degrees, the crest value of the flux is still 1.866 which is only a slight reduction from the maximum of two for $\theta=0$.

The transient inrush current for typical transformers is shown in Figures 15 and 16. It is characterized by being almost entirely unidirectional, rising abruptly to its maximum value in the first half cycle and thenceforth decaying until normal steady-state magnetizing current is reached.

The mechanism by which this decay is accomplished is illustrated in Figure 2. It will be noted that the inrush current has modified the impressed voltage on the terminals of the transformer by a small value equal to the voltage drop produced by the series resistance *R*. Examination of the resultant voltage curve shows that the effect of this drop is to decrease the area of the positive voltage wave and to increase the area of the negative voltage wave, thus making the voltage applied to the transformer asymmetrical. As the transformer flux is strictly proportional to the areas of the impressed voltage, it is evident that under these conditions, the positive flux generated in each cycle in the transformer is less than the negative flux generated by an amount equal to

$$\Delta\phi=\int_0^{2\pi} Ridt$$

where $\Delta\phi$ is the flux change per cycle. From this it is evident that the flux and corresponding inrush immediately begin to decay an amount which is largely a function of the resistance of the circuit and that the decay will continue until the magnetizing current of the transformer becomes symmetrical, that is, to normal value of the magnetizing current.

It might appear that the core loss also may be a factor influencing the rate of decay of the inrush current. If the eddy-current losses are considered this may be partially true during the first few cycles when the rate of decay is highest and appreciable eddy currents might be induced thereby. So far as the hysteresis loss is concerned, its influence is for all practical cases absent.

Factors Affecting the Magnitude of Inrush Currents

SOURCE IMPEDANCE

Impedance between the transformer and the source of sustained voltage acts to reduce the magnitude of the peak inrush current. Simultaneously a reduction in voltage at the transformer terminals takes place. This is illustrated by means of oscillograms shown in Figure 3. Figure 4 indicates the reduction in the peak inrush current for a typical modern single-phase transformer, as the source impedance is increased.

A study of Figure 4 in connection with the corresponding voltage wave shapes shown on oscillograms of Figure 3 reveals the fact that when inrush currents are of the highest magnitude (no system impedance), the voltage distortion is at its least. Generally in practical systems there will be some external impedance which will tend to reduce the inrush current below the maximum that could be obtained if the transformer were connected to an infinite bus. Thus, if the transformer is small as compared to the system, which will be the more common case, relatively high inrush currents may be obtained. However, with a system reactance of 1.18 per cent on the transformer base, for example, the system voltage as shown in oscillogram 116-2, Figure 3, is not seriously affected even though the inrush current is 21 times normal full-load current for the transformer. The general effect of source impedance on system voltage indicated by these oscillograms is in good quantitative agreement with some unpublished results based on calculations made by Edith Clarke. The relationship between source reactance and system rms voltage at the transformer terminasl during the inrush period is shown in Figure 5.

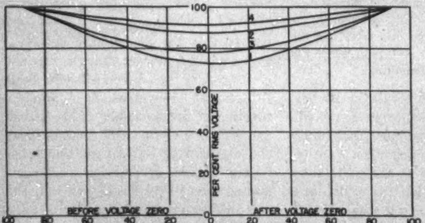
SYSTEM VOLTAGE LEVEL

It is well known that the normal exciting current of a transformer varies essentially linearly with applied voltage up to the point where saturation of the iron begins to take place. Increasing the voltage beyond this point produces more than a proportionate increase in the exciting current. In the case of maximum transient inrush current, however, there is a linear relation between maximum peak inrush current and applied voltage which prevails over a wide range of applied voltage, both above and below the rated voltage of the transformer. This is indicated by Figure 6 which shows the results of tests on a miniature-system single-phase transformer. The straight-line relationship is explained by the fact that under the conditions of residual flux and switching angle which produce the maximum inrush current, the iron is completely saturated. Hence the transformer impedance which limits the inrush current is essentially the resultant of the air-core reactance of its windings as previously pointed out. Under maximum inrush conditions, this remains constant over a range of applied voltage extending upward from approximately 60 per cent of rated transformer voltage.

While large initial inrush currents are most frequently encountered in transformers connected at system locations where the voltage is relatively high, as for example at generating stations or close to substation busses, it is important to note that the relation between inrush and voltage is essentially a linear one; not second or third power relationship as might be assumed from familiarity with normal exciting-current characteristics. In this connection, it seems probable that the few cases of trouble which have been experienced from inrush currents of transformers installed at points of relatively high system voltage have been caused as much by the low system reactance viewed from these points as by the relatively high operating voltage level.

Figure 8. Effect of switching angle on system rms voltage

Residual of magnitude and polarity to give maximum inrush. Source voltage normal as defined by Figure 12. Single-phase transformer, assuming 3 per cent normal exciting current. Base ohms = 450 ohms



1. 5.5 per cent system reactance—curve *A*, Figure 12
2. 5.5 per cent system reactance—curve *B*, Figure 12
3. 2.75 per cent system reactance—curve *A*, Figure 12
4. 2.75 per cent system reactance—curve *B*, Figure 12

SWITCHING ANGLE

It has been pointed out that when a transformer is energized at a zero point on the voltage wave, the maximum inrush current will be obtained. As the switching angle is varied in either direction from a voltage zero the crest inrush current is reduced. Figure 7 shows the relation between crest inrush current and angle of switching, as obtained from tests on a miniature single-phase transformer. Figure 7 also indicates the effect of residual flux. The upper curve shows the inrush current for the condition of maximum residual of the polarity to produce the maximum flux. The lower curve is for the condition of no residual flux. If a residual of opposite polarity exists, then the current resulting from switching at voltage zero will be still lower.

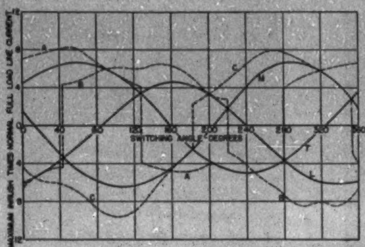
Since any instant of closing may be realized in practice, the effect of switching angle and the resulting inrush current on system rms voltage becomes of interest in determining the range of variation of system voltage for various switching angles. This effect is shown in Figure 8. In connection with Figures 7 and 8, it is recognized that for relatively high voltages and for slow rates of contact travel, prestriking may occur before mechanical closing of the switch contacts takes place. Hence the probability of closing at a voltage zero is reduced.

THREE-PHASE SWITCHING

In energizing a three-phase transformer, the inrush current will generally be quite different in the three phases. Furthermore, modification may be introduced by the nonsimultaneous closing of all poles of the switch.

In order to get a clear picture of the effect of switching angle on inrush in the case of a three-phase core-type miniature-system transformer, tests were made using a delta-connected winding. All poles were closed simultaneously, and the instant of closing the switches was varied throughout a 360-degree range. The results are shown in Figure 9.

The winding currents are seen to line up properly with the instant of closing as



T—current in top leg
M—current in middle leg
L—current in lower leg
} delta currents

A—line current = *T* − *L*
B—line current = *L* − *M*
C—line current = *M* − *T*

Delta currents should be multiplied by $\sqrt{3}$ to become times normal delta currents

Three-phase transformer delta-connected operated under normal conditions as indicated in Figure 12. No external impedance. Three per cent exciting current assumed, base ohms = 100 ohms. Three phases closed simultaneously. When phase c voltage is a maximum, switching angle is 90 degrees

Figure 9. Effect of closing angle on maximum crest inrush currents

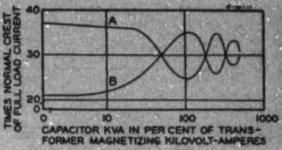


Figure 11. Effect of capacitor on maximum transformer magnetizing inrush currents

Transformer de-energized under normal operating flux-density conditions leaving the capacitor at normal voltage to discharge through the transformer each time before the transformer was again energized at an instant controlled to give maximum inrush. Saturation curve *A* of Figure 12 with transformer operating at 110 volts

Curve *A*—Last half-cycle of current in transformer before switch was opened of same polarity as first half-cycle of inrush current

Curve *B*—Last half-cycle of current in transformer before switch was opened of opposite polarity to the first half-cycle of inrush current

should be expected; that is, each winding behaves as if it were a single-phase unit. However, the line currents are determined by the sums of two winding currents. This sum can never be greater than the arithmetical sum of the two winding currents and never less than the difference. The polarity of the maximum crest will be that of the greater of the two winding currents. As the instant of closing is varied, passing through that instant which causes two winding currents to be equal, the polarity of the maximum peak line current will suddenly reverse as shown in this figure.

SEQUENTIAL SWITCHING

In three-phase switching it is possible and likely that all poles of the breaker do not close at exactly the same instant. In order to show what the maximum possible inrush currents would be as compared to those obtained for simultaneous closing for all possible transformer connections, Table I has been prepared.

The results presented here show that based on normal line currents, the wye-grounded transformer with delta secondary gives the highest inrush while the lowest is obtained with the delta-connected primary winding, with the secondary either wye or delta. This is true for either three-phase core-type transformers or for three-phase banks of single-phase transformers.

Table I also shows that sequential switching in a three-phase transformer increases the inrush by about ten per cent only for the wye-grounded primary with secondary either delta or wye. For other connections, there is no increase due to sequential switching.

For single-phase banks, the wye-connected primary gives no increase due to sequential switching for any condition except when the secondary is delta-connected. For this case an increase of about ten per cent was observed. For the delta-connected primary, the increase in magnitude of inrush current was 50 per cent with the secondary either wye or delta.

In connection with Table I it should be pointed out that substantial reduction in inrush-current magnitudes can be obtained by energizing the winding farthest from the core. Since the absolute ceiling on magnetizing inrush crest current is determined by the air-core reactance of that winding, it follows that the winding having the greatest air-core reactance should have the lowest inrush. For the single-phase miniature-system units, it was found that the inrush currents could be cut in half by energizing the winding farthest from the core rather than the winding nearest the core. Thus, other things being equal, for concentric windings, there is some practical merit in energizing transformers on the high-voltage side, since that winding, ordinarily being farthest from the core, will generally have an appreciably higher air-core reactance.

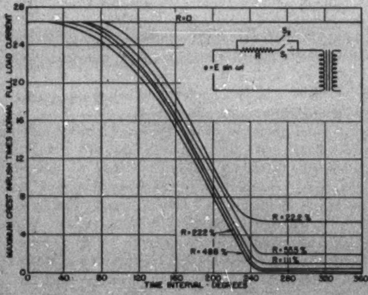


Figure 10. Effect of resistor switching on inrush currents.

Single-phase transformer. Source voltage normal as defined by curve *A*, Figure 12. Residual of polarity to give maximum inrush. Time lag is interval between closing S_1 and S_2. For any given time lag, the inrush given by these curves is the maximum possible for the conditions specified

Characteristics Affecting the Duration of Inrush Current

In the case of transformers connected to systems having negligible source resistance, the transient inrush currents decay at a rate determined by the resistance of the transformers themselves. Tests on representative power transformers, both single-phase and three-phase, indicate that for ratings in the neighborhood of 1,000 kva, the crest inrush current decays to approximately half its initial peak value in ten cycles. With larger transformers, the rate of decay is slower, the inrush current sometimes lasting a second or more in extreme cases. Conversely, smaller transformers generally have higher rates of decay. The inrush current of a typical small distribution transformer, for example, may be only approximately 20 per cent of its initial crest value after ten cycles. Transformers having relatively high losses

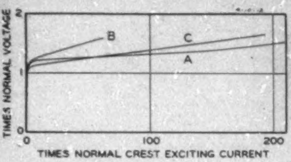


Figure 12. Saturation curves for miniature-system transformers

Constants are for 60 cycles per second

A. Single-phase transformer. Base voltage (normal) 100 volts rms. Magnetizing reactance $x_m = 15{,}000$ ohms at 100 volts rms

B. Same as *A* but less abrupt saturation

C. Three-phase core-type transformer. Average of three legs. Average magnetizing reactance, $x_m = 10{,}000$ ohms at 100 volts rms. Base voltage (normal) = 100 volts rms

will in general have faster rates of decay than those which are more efficient.

Resistance in the circuit between the transformer terminals and the source of sustained voltage not only reduces the maximum initial inrush current that can be obtained but hastens its rate of decay where only one transformer is involved and aids in restoring normal voltage wave shape more quickly. Source reactance does not alter the rate of decay of inrush currents for practical cases.

The effect of system resistance in causing circulating magnetizing currents to flow when one transformer is energized on the same bus with another which is already energized has been investigated. The summary of that investigation is included in reference 10.

Means for Reducing Magnitude of Inrush Currents

Switching With Resistance

The use of resistance as an intermediate step in closing a switch to reduce the inrush current was investigated with the miniature system. The results are shown in Figure 10. In these tests, the interval between the instant of closing S_1 and the instant of closing S_2 was varied through 360 electrical degrees. For each value of this interval, the synchronous switch was adjusted to give the maximum inrush for the particular value of resistance being used. Thus for the given transformer, and for the resistance used, the curve shows the maximum peak inrush current that can be obtained, corresponding to any interval between the closing of the switches.

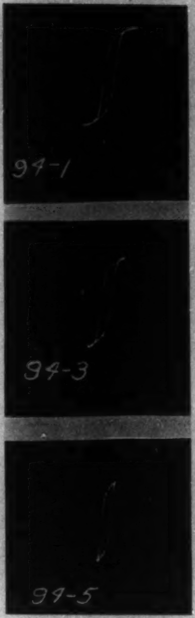


Figure 13. Hysteresis loops for single-phase miniature-system transformers

Saturation curve *A* of Figure 12. 25-ohm resistor in series to give voltage drop for amplifier to oscillograph

Oscillogram Number	60-Cycle Rms Voltage
94-1	110
94-3	102
94-5	90

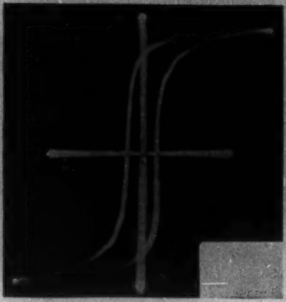


Figure 14. Hysteresis loop for modern silicon steel as used in typical power transformer

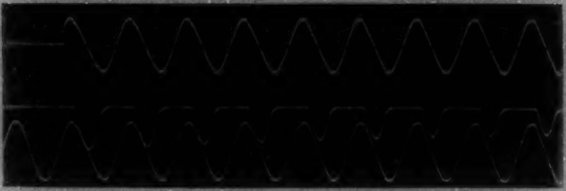
It is significant that for relatively short intervals on the order of 180 electrical degrees the inrush current is approximately cut in half for practical values of resistance. For time intervals up to about 200 electrical degrees, the value of resistance (for reasonable values) is not nearly so important as the time interval. For greater values of time interval the resistance is practically the sole determining factor, although the magnitude of inrush current is so low for any interval greater than 200 degrees that a wide range of resistance values will accomplish the desired result. Thus, if the time interval between closing S_1 and S_2 is 200 degrees or more, maximum inrush currents can be reduced to half value by using a resistor of ten per cent ohms on the transformer base. A higher resistance is even more effective, although it is apparent that if this resistance becomes so high as to limit appreciably the flow of normal magnetizing current it will again lose its effectiveness. The important conclusion to be drawn from this is that breakers equipped with resistors useful in preventing switching overvoltages[9] on opening may also be helpful in reducing inrush currents on closing.[11]

Means of Reducing Residual Flux

Since one of the factors tending to increase inrush currents is the residual magnetism, it follows that any means of reducing it would have some merit. The possibility of mechanical jarring, for instance, was tried and found to have only slight effect.

The possibility of using a capacitor connection in parallel with the transformer to cause a damped oscillation to occur when the switch is opened has been suggested as a means for eliminating the residual. Following the opening of the switch, the oscillation theoretically should cause an alternating current to flow in the transformer winding decreasing in amplitude with time, thus gradually reducing the area of the traversed hysteresis loop until, when the current oscillation has died away, no residual remains.

Such an arrangement was tried, and results as shown in Figure 11 were obtained. These results show that capacitors can eliminate the residual magnetism and that there is a minimum size of capacitor that can do this. Smaller capacitors cause the circuit to be more than critically damped rather than oscillatory so that the current flowing in the transformer due to the capacitor discharge just after the switch is opened does not oscillate at all. In fact, the amplitude of the current never reaches that re-



quired to bring the residual to zero. However, as the capacitance is increased, the crest current during discharge increases without oscillation until a value is reached which is just high enough to eliminate the residual. This optimum value corresponds to capacitor kilovolt amperes of from 40 to 50 per cent of the transformer magnetizing kilovolt-amperes.

An increase in capacitance beyond this optimum value causes the crest current during capacitor discharge to exceed that required for eliminating the residual. Thus, a residual of opposite polarity is introduced and reaches a maximum when the capacitor kilovolt-amperes equal the transformer magnetizing kilovolt-amperes. In this area, the discharge begins to be oscillatory so that, as the capacitance is increased, the first oscillation of current due to discharge from the capacitor reverses the residual, but the second oscillation reverses this. Consequently, as the capacitance is increased, a value is reached which causes this second oscillation to reduce the residual to zero. Further increase of capacitance causes similar phenomena involving more reversals during discharge, which result in a number of values of capacitance which can reduce the residual to zero.

Subsequent tests revealed that the effectiveness of the capacitor was relatively unchanged by the amount of residual in the practical range. This suggests that a capacitor selected in accordance with the foregoing results could be of some practical value in partially reducing transformer inrush currents.

Figure 15. Oscillogram showing inrush current transient in 833-kva single-phase transformer

Upper trace—Transformer terminal voltage
Center trace—Transformer current
Lower trace—Source voltage

Miniature-System and Test Data

Miniature-System Transformers

The saturation curves for the miniature-system transformers are shown in Figure 12. These have Mumetal cores in order to give the desired abruptness of saturation. When operating at 100 volts rms, the saturation characteristics correspond very nearly to those of some of the newer silicon strip steels when used at an assumed density of 100 kilolines per square inch. At 100 volts, the exciting impedance of the single-phase units is 15,000 ohms. Their leakage reactance is 25 ohms at 60 cycles, and their average d-c resistance is approximately 5.5 ohms for the winding nearest the core and 11.0 ohms for the outside winding. Curve *A* of Figure 12 is the saturation curve for these units while curve *B* is for another transformer identical in every respect except for less abrupt saturation.

For the three-phase core-type units, the resistance of the windings nearest the core is 7.0 ohms and of the winding farthest from the core, 24.0 ohms. The saturation characteristic shown in Figure 12, curve *C*, was obtained with the transformer connected in delta, currents being measured in the transformer windings and not in the line. In the line, the third harmonic and multiples thereof do not flow. Consequently, the apparent saturation is much less abrupt when line currents are used.

For the single-phase transformers saturating as in Figure 12, curve *A*, the hysteresis loops are as shown in oscillograms 94-1, 94-3, 94-5, Figure 13. For excitations of the order of 100 volts, the hysteresis loop is such as to yield residual flux of the order of 50 to 60 per cent. Thus, while the saturation characteristic appears to be similar to that of new silicon steels, the residual is somewhat less than that associated with such steels. The residual in these units is more on the order of that for transformers using the old types of sheet steel. However, this does not invalidate the qualitative results obtained, although direct numerical checks cannot be made in some cases. A hysteresis loop for typical modern-power-transformer steel, indicating approximately 80 per cent residual, is shown in Figure 14 for comparison.

Tests on Actual Transformers—Comparison With Calculated Values

(*a*). *Single-Phase Transformer.* This unit was a 60-cycle core-type design rated 833 kva. The core steel was of the new high-permeability high-saturation type, and the unit was energized from a 2,300-volt winding. The calculated peak inrush current was 14 times peak full-load current based on an assumed 75 per cent residual and a core saturation flux of 130 kilolines per square inch.

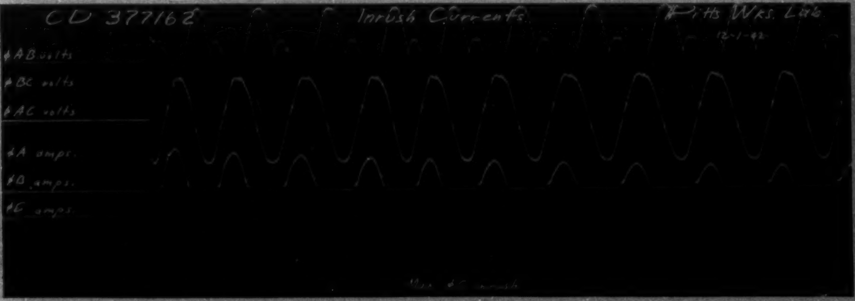


Figure 16. Oscillogram showing inrush current transients in three-phase 750-kva transformer

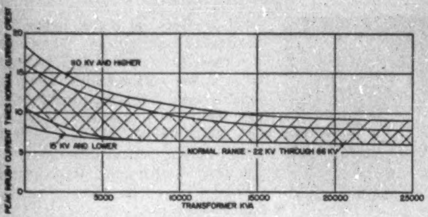


Figure 17. Calculated inrush characteristics of single-phase transformers

Excitation on low-voltage side

The maximum measured inrush current obtained in 30 trials was 11 times the peak full-load current. The oscillogram for this test is shown in Figure 15. The switch was not closed exactly at voltage zero but nearly enough to zero so that the maximum inrush should not be appreciably greater, based on the earlier considerations in this paper.

(*b*). *Three-Phase Transformer.* This unit was a 50-cycle core-type water-cooled design rated 750 kva. It was excited from its 3,000-volt windings which were delta-connected. The calculated inrush was 12.5 times peak-line full-load current based upon 75 per cent residual and core saturation of 130 kilolines per square inch.

The maximum inrush current obtained in 20 trials was 10.35 times peak-line full-load current. This is shown in the oscillograms of Figure 16.

(*c*). *Generalized Calculated Values of Inrush Currents.* A more comprehensive summary of calculated inrush currents is shown in Figures 17 and 18. This summary includes numerous designs and is of help in showing the practical limits between which the maximum inrush current will likely fall for various voltage levels and for various kilovolt-ampere sizes Generally, calculations tend to give too high a value as indicated by the foregoing examples. Consequently, actual maximum values will generally be slightly less than the values indicated by these bands. In the aforementioned test cases, part of the discrepancy between calculated and test results is attributable to the fact that generator source used in the test was not actually an infinite bus, while calculations were based upon the source being an infinite bus. These calculations also indicate that for typical 25-cycle and 60-cycle transformers, there is no appreciable difference in the peak inrush currents.

TESTS TO DETERMINE HEAT GENERATED IN FUSES DURING INRUSH PERIOD

Essentially all of the heat developed in a fuse during the transient inrush period goes into raising the temperature of the fusible element, very little being dissipated elsewhere during the short interval involved. Hence, the following equation may be written, applying to the fuse during the inrush period.

$$I^2t = K$$

where

I = current in the fuse, in amperes
t = time in seconds
K = a constant, depending on the size and material of the fusible element

The I^2t of any given fuse may be determined from the manufacturer's published characteristics, usually given in curve form, and plotted on logarithmic coordinates so that this portion is represented as a straight line. It is only necessary then to know the I^2t corresponding to the transformer magnetizing inrush current, in order to select a fuse which will withstand it safely. While the I^2t representing the inrush for any specific transformer is of course a function of both the magnitude of the first current loop and the rate of decay, neither of which is usually known without a rather exhaustive study of the transformer and the system to which it is connected, a series of tests has indicated that the total I^2t of the inrush may be approximated with sufficient accuracy for purposes of fuse application. In these tests, for which the authors are indebted to H. C. Stewart of the General Electric Company, oscillograms were taken of the inrush currents of representative samples from a complete line of transformers, under conditions designed to produce the greatest inrush current and the slowest rate of decay. Although the maximum-crest inrush currents of the several sizes of transformers tested ranged from 11 to 20 times their normal full-load crest currents, and the intervals required for these transient currents to decay to normal value extended from 33 to 120 cycles, it was found that for each transformer tested the total I^2t in its magnetizing inrush was numerically almost the same as if ten times normal full-load current had been flowing for 0.1 second. This leads to a rather simple criterion for selecting fuses which will safely withstand transformer magnetizing inrush currents. When what appears to be a reasonable margin in excess of the actual I^2t in the transformer inrush is allowed, it is suggested that fuses be selected on the basis that at the 0.1-second point on their characteristic curves, they be capable of carrying at least 12 times the transformer normal full-load current. It will be apparent by referring to the equation $I^2t = K$ that any other pair of corresponding figures besides 12 and 0.1 could have been used. These values, however, have the advantage of being round numbers which are easily remembered, and at the 0.1-second point, most fuses adhere closely to the straight-line characteristic. Fuses selected on the basis outlined may be expected to be amply large for the majority of applications, since it is unlikely that actual inrush currents will be as severe or prolonged as those obtained under the carefully controlled test conditions, and where the testing source was so much larger than the transformer as to be practically an infinite bus. Furthermore, the transformers tested were of a design that is considered fairly representative of modern transformer designs with regard to the severity of inrush current to be expected.

Conclusions

1. The inrush currents associated with transformer energizing do not, in the vast majority of cases, produce any system disturbances. This is true even for transformers utilizing the modern steels and de-

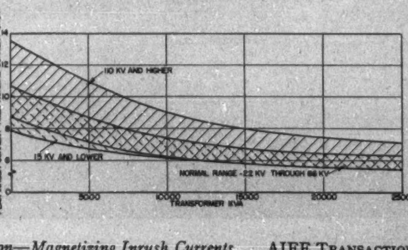


Figure 18. Calculated inrush characteristics of three-phase transformers

Excitation on low-voltage side

The Geometry of Arc Interruption—II Current-Zero Phenomena

E. W. BOEHNE
FELLOW AIEE

Synopsis: During the microsecond period in which an interrupter is performing the act of changing from a conductor to an insulator (termed interruption) the current, voltage, and arc resistance undergo variations which materially affect the performance characteristics of the interrupter. The nature of these variations depends upon the medium of interruption, design features of the interrupter, and the constants of the current and recovery-voltage circuits. Modern power interrupters employing air present variations in the current-zero region of an entirely different type from that observed in the fluid interrupters. A knowledge of the nature and cause of these variations is becoming of more importance from a design performance and application viewpoint.

The rapidly varying resistances within the interrupter during the process of interruption have always presented obstacles to the analyst. In reapproaching this problem on the basis of an orderly, yet practical, variation of circuit current in the region of current zero and allowing the parameter of resistance to become secondary, the problem reduces to one of linear constants and can be solved by standard methods. This paper presents such a treatment. It is generalized in such a way as to include all practical types of current-zero phenomena generally encountered. Recovery-voltage characteristics are literally fabricated from "standard building blocks" of which relatively few are required. The method is flexible enough to be applied to any assumed current variation in the region of current zero. The analysis is presented in terms of the accepted nomenclature of circuit theory. The operational attack is employed.

Current-zero phenomena are closely associated and largely dependent upon the events which take place during the arcing period. This action has been previously described.[1] Correspondingly, the unique current-zero characteristics of the Magne-Blast air circuit breaker gave rise to this generalized study. The application of this work to recovery rates, d-c interruptions, and allied phases of circuit interruption is discussed.

THE final proof of the ability of an a-c circuit interrupter to sever the circuit, whether it be a circuit breaker, fuse, lightning arrester, or rectifier, is determined over the very short interval in the region of current zero. At each current zero in the arc or conducting medium the important thermal actions of the arcing or conducting period are integrated[1] and pitted against the recovery-voltage transient to determine the outcome, the success of which is termed interruption. The environment of the arc, not only the medium of interruption but the nature of the adjacent circuits,[2,3] plays an important role in the phenomena of circuit interruption. The arcing period, in particular, is capable of having a tremendous influence upon the current-zero phenomena.[1] In addition, however, the circuit interrupter is capable of imparting changes in the normal current flow in the region of current zero which play an important part in the final outcome.

It is the purpose of this paper to present a more general attack of these current variations in the region of current zero; to present for the first time some heretofore unsuspected current variations in this microsecond interval; and to coordinate these considerations with the general problem of circuit interruption.

The shortness of the interval, the relative magnitude of the current just prior to the current zero, the lack of a means of measuring a rapidly varying dielectric strength, all add to the difficulties of measuring the important events which take place over this interval in power interrupters. As a result of these difficulties the interpretation of the phenomena which take place in this brief interval, with few exceptions,[4] has been reported in qualitative physical terms which are not all subject to oscillographic measurement. Although a description of the current-

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signed for operation at relatively high flux densities, because only under a very rare combination of circumstances can the theoretically possible maximum inrush current be obtained. In any practical systems, the effect of source impedance in reducing the peak inrush currents and shortening their duration, together with the probabilities regarding the direction of residual flux and instant of closing will usually result in negligible system disturbance.

2. In those cases where tripping of differential relays occurs too frequently to be tolerable during the inrush period, harmonic-restraint relays or desensitizing devices can be used to overcome the difficulty.

3. The damaging of fuses during the inrush period can readily be avoided by proper fuse-application practices.

4. In those relatively few cases where it is desirable to minimize the magnetizing current inrush, several helpful measures are suggested.

(*a*). Energizing on the winding having the higher air-core reactance when possible.

(*b*). The use of resistor switching (two-step).

(*c*). The use of a parallel capacitor of from 40 to 50 per cent of the normal transformer magnetizing kilovolt-amperes to eliminate the residual magnetism.

(*d*). Energizing on the delta winding rather than wye if there is a choice, other things being relatively equal.

5. From the standpoint of mechanical stresses, inrush current is not a special problem because its maximum peak value can never be as high as the maximum possible peak value associated with a through fault with maintained primary voltage, which is a condition that the transformer may be subjected to in actual service.

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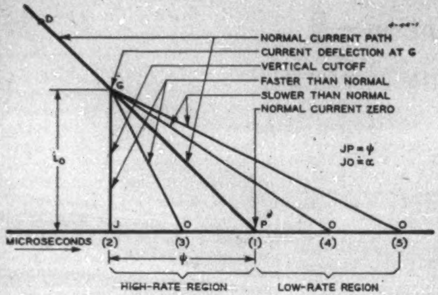


Figure 1. On the normal current path to zero, *DGP*, the current is assumed diverted at G to take a new linear path to zero

This diagram defines the pendulum action of the current in the region of current zero and the resulting high-rate and low-rate regions

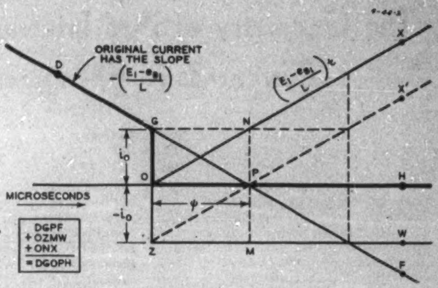

ORIGINAL CURRENT HAS THE SLOPE
$-\left(\frac{E_1-e_{a1}}{L}\right)$
$\left(\frac{E_1-e_{a1}}{L}\right)t$
D
X
X'
G
N
O
P
H
i_o
$-i_o$
ψ
MICROSECONDS
Z
M
W
F
DGPF
+OZMW
+ONX
=DGOPH

Figure 2. Special type of current-zero phenomena in which the current is suddenly interrupted at I_o

The resultant path *DGOPH* is composed of the original path *DGPF* plus the rectangular cancellation current *OZMW* and the triangular cancellation current *ONX*

zero phenomena is of assistance to the understanding of the problem and in many cases is the forerunner of the quantitative analysis, it still remains that the arc in the interrupter becomes a parameter of the electric circuit and is capable of a more rigorous treatment in electrical nomenclature. This paper is devoted to this philosophy and will include mention of the physics of what is taking place only as a guide in the consideration of possible cases.

On inspection of the current-zero region, even with the assistance of all modern measuring equipment, the largest single missing factor is the current variations. The voltage variations are measured accurately by the cathode-ray oscillograph. If all the external parameters of the circuit are available, together with the transient voltage which appears across the arc gap, it is a mathematical fact that the nature of the current over this interval is then capable of determination. With the current and arc-gap voltage both available, the arc resistance and, of more importance, the arc-gap energy during this interval are made available. This information to the physicists should enable a still more accurate description of the physics of this important period. This suggests that particular known circuits and resulting recovery transients be analyzed to compute the current variations in the current-zero region. This has been done and gave rise to the treatment presented here. Such an analysis is tedious and when completed gives the result for only one particular case and is of little general value. A new approach was then sought which would permit of greater generalization and was found in the expedient of arbitrarily permitting the current in the region of current zero to vary in a definitely prescribed manner and to compute the recovery characteristics which would result. On the basis of many specific cases in which the true current was computed from the cathode-ray records, covering many types of interrupters, a pattern of the current variation was chosen which is extremely simple and approximates the range of phenomena to be expected with a faithfulness exceeding the original expectations.

Operational methods of attack, coupled with a simple form of superposition[5] termed the cancellation principle, permit a maximum degree of flexibility to be achieved. The resulting recovery-voltage curves are literally fabricated from a stock of "standard building blocks" of which only relatively few are required to build all observed recovery-voltage phenomena. The work will be of interest to the student of operational methods. The recovery-voltage "building blocks" are derived in the appendixes of the paper and will be found useful in constructing recovery-voltage phenomena beyond the scope of those presented in this paper.

Thesis of Attack

By the simple expedient of permitting the current to be pivoted at a small instantaneous magnitude i_o, which occurs ψ degrees ahead of the normal current zero, and permitting the current to approach zero in a straight line at any desired slope, both greater and slower than the normal path as shown in Figure 1, the vast majority of recovery-voltage phenomena observed in power circuit breakers can be closely approximated. In this pendulum action of the current two special cases occur, namely:

1. The normal approach to current zero.
2. The vertical cutoff of current at i_o.

The former is the assumption in computing the standard rates of rise of recovery voltage in power circuits.[2,3,6] It produces a true measure of the circuit severity in which the breaker plays no part; a severity which the breaker is given credit for overcoming,[3] regardless of the manner in which it controls the current in the region of current zero. The vertical cutoff of current at i_o, ψ degrees ahead of the normal current zero, is a special case of maximum severity,[4] which, if taken to extreme can produce dangerous overvoltages,[7] particularly in circuits having a high natural frequency.[8,9]

The scope of the cases considered by the pendulum swing of the current from the vertical cutoff case, through the normal rate of decay, to rates of decay considerably lower than normal may be divided in general into two groups (see Figure 1).

A. Those which decay faster than normal, or the high rates.

B. Those which decay slower than normal, or the low rates.

Although not subject to such a clean-cut division as suggested here, the evolution of the physics of the interrupting phenomena is of interest. It is shown in the paper that, in general, the high rates are capable of producing accelerated rates of arc-gap resistance which are capable of approaching extremely high values in the region of the true current zero The low rates on the other hand are shown, in general, to be associated with arc-gap-resistance changes which are slower than normal and in some cases are character-

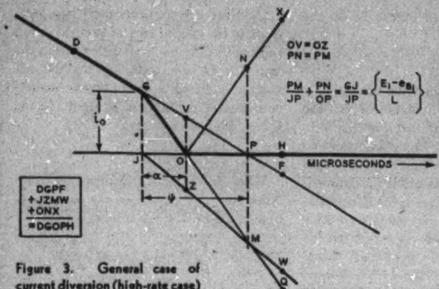

X
D
N
OV = OZ
PN = PM
$\frac{PM}{JP}+\frac{PN}{OP}=\frac{GJ}{JP}=\left\{\frac{E_1-e_{B_1}}{L}\right\}$
G
V
i_o
P
H
J
O
F
MICROSECONDS
α
Z
ψ
DGPF
+JZMW
+ONX
=DGOPH
M
W
Q

Figure 3. General case of current diversion (high-rate case)

The resultant path *DGOPH* is composed of the original path *DGPF* plus two triangular cancellation currents, *JZMW* and *ONX*

ized by glow-discharge states within the interrupters, which are strong enough to reduce tremendously the recovery transients without permitting the circuit to be reignited. Interruptions under these conditions are thus independent of the recovery severity of the circuit. The high-rate phenomena are, in general, associated with oil-breaker performance at moderate currents, low-rate phenomena not being unusual at high currents. Low-rate phenomena are, in general, prevalent in the performance of air interrupters of the magnetic type and have been mastered[10] in an unusual manner in the design performance of the Magne-Blast air circuit breaker.[11] At extremely light currents, however, the performance of these breakers trends slightly into the high-rate region.

The Cancellation Principle

Cancellation, as the term applies to this problem, consists of the introduction of currents into the circuit in such a manner that the sum of the original unaffected current and cancellation currents produces the current path desired. The net sum of all voltages resulting from each of the currents, including the original, produce the resulting recovery characteristic sought. Consider the special case of the vertical cutoff of i_o amperes as portrayed in Figure 2. The current is approaching zero along the line *DGPF* and would reach zero at *P*. The slope of this line is determined by the history of the arcing period and will be discussed more fully later. At the point *G*, i_o amperes, the current is suddenly interrupted. This result is accomplished by adding to the original current, the cancellation current *OZPX′*, which in turn is made up of the rectangular component *OZMW* of $-i_o$ amperes and the triangular component *ONX* which has a slope opposite to that of the original current.

By the principle of superposition it follows that the voltages which appear across the circuit as a result of the introduction of these two cancellation currents, when added to the voltages present as a result (or cause) of the original current, will produce the net recovery-voltage characteristic sought. The special case mentioned heretofore introduces the only two types of cancellation currents necessary to approximate closely the vast majority of recovery-voltage phenomena encountered in power interrupters.

General Case

Before considering the circuits of practical importance in which the aforementioned phenomena occur, consider the more general case revealed in Figure 3. Here at *G* the current is diverted to approach zero at a rate faster than normal. In this case the two cancellation currents are both triangular in form, namely *JZWM* and *ONX*. Unlike the special case of Figure 2 the two cancellation currents must be introduced into the circuit a time interval *JO* apart. By plane geometry it can be shown that the sum of the slopes of the two triangular cancellation currents is equal to and of opposite sign to the original current slope—a fact true of Figure 2 also. This fact simplifies the ultimate expressions of the recovery voltages.

In a similar manner, Figure 4 reveals a new case of considerable interest in which the current is diverted at *G* to decay at a rate slower than normal. This phenomenon is frequently accompanied by glow-discharge states within the interrupter which, as will be shown, react to minimize the recovery-voltage transient. Here again the sum of the slopes of the two cancellation currents is equal and opposite to the slope of the initial current. Other current-zero phenomena are revealed in Figure 5 all of which may be similarly fabricated by cancellation components, of the two types mentioned.

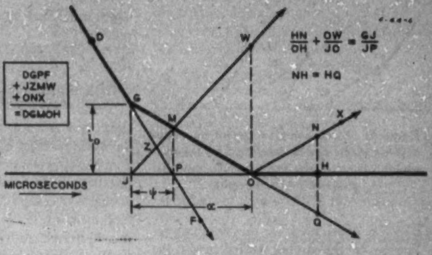


Figure 4. General case of current diversion (low-rate case)

The resultant path *DGMOH* is composed of the original path *DGPF* plus two positive triangular cancellation currents *JZMW* and *ONX*. Each of these three currents is responsible for a corresponding component of the resulting recovery voltage

Effect of the Arcing Period[1]

The slope of all the triangular cancellation currents in the preceding analysis is dependent upon the slope of the initial current *DGP* as it approaches zero. This slope in turn is a function of many factors, including the total history of the arcing period to the current zero in question. A method of determining this current slope has been fully described[1] previously and bears a striking resemblance to this analysis, the arc voltage being employed as the cancellation parameter. In all cases the slope of the current *DGP* is

$$\left\{\frac{E_1-e_{B_1}}{L}\right\} \tag{1}$$

where, in the nomenclature of the previous paper[1]

E_1 = the instantaneous magnitude of the generated voltage, E, in the neighborhood of current zero

e_{B_1} = corresponding magnitude of the arc voltage e_B. Except for leading phase-angle interruptions of zero-power-factor (lagging) circuits, e_{B_1} is numerically negative with respect to E_1

L = the inductance which limited the current

The voltage $(E_1-e_{B_1})$ is plotted in Figure 6 as a function of the arc voltage e_B for zero-power-factor symmetrical short circuits which have experienced arc voltages for only one loop of current. This function is presented in Figure 6 for both the flat-top and linearly rising arc-voltage characteristics. These curves

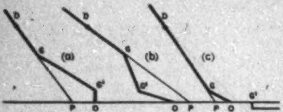


Figure 5. More complex current-zero phenomena which may be fabricated by the summation of rectangular and triangular cancellation current components

may be considered as the nominal influence of the arcing period upon the recovery-voltage characteristic. Initial displacement, various angles of arc-voltage initiation, multi-half-cycle arcing, and so forth, will all cause variations in this basic coefficient of the recovery-voltage phenomena.[1] When the arc voltage is assumed zero in the symmetrical zero-power-factor circuit, the aforementioned slope equation for the current reduces to E_m/L. This is the value used in computing the basic recovery rates of the circuit independent of the action of the breaker.[2,3]

The Circuits

Having held the number of cancellation-current components to a minimum of two, let us consider the minimum number of circuits necessary to cover the practical field of application. As in a previous study of circuit-recovery rates[3] the division into single-frequency and double-frequency circuits becomes clearly defined.

The single-frequency circuits employing linear, lumped parameters are shown in Figure 7. It can be shown that the series resistance r of the circuit of Figure 7b can be transformed effectively to the shunt resistance R of Figure 7c by the relation

$$R = L/rc \tag{2}$$

In a similar manner the resistance r of Figure 7d may be combined with the shunt resistance R by first converting to a parallel resistance by means of equation 2 and adding the two shunt resistances in parallel. In both cases this procedure is accurate to a high degree for low-power-factor circuits although subject to some second-degree corrections which are so small as to be readily neglected. Since the circuit of Figure 7a may be obtained by permitting the shunt resistance of Figure 7c to approach infinity, it follows that we have but one single-frequency circuit to consider, namely Figure 7c. The basic building blocks of the recovery-voltage characteristics for single-frequency circuits may therefore be determined by injecting the two cancellation currents, $-i_o$ and $(E_1-e_{B1})t/L$ into the parallel impedance of R, L, and C of Figure 7c. The solution of this circuit for the rectangular and triangular cancellation currents is presented in Appendix A. The solutions are

1. For the rectangular cancellation current, i_o, applied to the circuit of Figure 7c, the voltage e' which appears across the circuit-breaker contacts becomes

$$e' = \frac{i_o}{\omega C}\epsilon^{-bt}\sin\omega t \tag{3}$$

where

$$b = \left(\frac{1}{2RC}\right);\quad \omega = \sqrt{\omega_o^2 - b^2},\ \text{and}\ \omega_o = \sqrt{\frac{1}{LC}}$$

2. For the triangular cancellation current $(E_1-e_{B1})t/L$ applied to the circuit of Figure 7c the voltage e'' which appears across the circuit-breaker contacts becomes

$$e'' = (E_1 - e_{B1})\left\{1 - \frac{\omega_o}{\omega}\epsilon^{-bt}\sin(\omega t + \phi)\right\} \tag{4}$$

where

$$\text{Tan}\ \phi = \omega/b$$

Consider the application of these relations to the special case of vertical cutoff as shown in Figure 2. It follows from the principle of superposition that the net voltage, e, which appears across the circuit-breaker contacts as a result of the three currents, becomes

$$e = e_{B1} + e' + e''$$

When these three relations are combined into one expression for the special case of Figure 2, there results

$$e = E_1 - \epsilon^{-bt}\left\{(E_1 - e_{B1})\cos\omega t + \frac{1}{\omega C}\times \left[-i_o + \frac{(E_1 - e_{B1})}{2R}\right]\sin\omega t\right\} \tag{5}$$

This result is in agreement with the classical solutions[4] of this special case presented in 1931, provided the term E in the previous solutions[4] be redefined as the instantaneous magnitude of the generated voltage. By permitting R to become infinity, equation 6 reduces to the solution of Figure 7a (Appendix A of reference 4). By replacing R with (L/rC) of equation 2, equation 5 reduces to the solution of Figure 7b (Appendix C of reference 4) in which b now becomes $r/2L$ and $\omega = \sqrt{\omega_o^2 - b^2}$. The circuit of Figure 7d, being capable of reduction to either Figure 7b or Figure 7c, has, therefore, been adequately considered.

In the numerical use of equation 5 it might be pointed out that i_o is numerically negative with respect to E_1 and

Figure 6a. Nominal relation between the current slope coefficient $(E_1 - e_{B1})$, the generated voltage at current zero (E_1), and the arc voltage of the flat-top characteristic e_B

For this type of arc voltage $e_{B1} = e_B$ (completely described in reference 1)

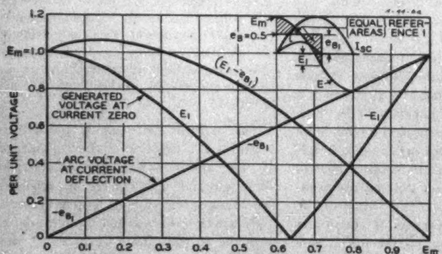
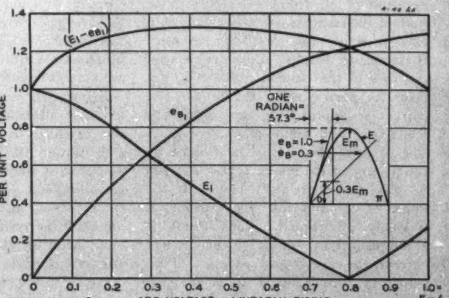


Figure 6b. Nominal relation between the current slope coefficient $(E_1 - e_{B1})$ the generated voltage at current zero, (E_1), and the arc voltage, E_B, of the linearly rising type defined in the figure

These relations hold for zero-power-factor conditions. They describe the coefficients of all the recovery-voltage equations and thus indicate the influence of the arcing period upon the recovery transient[1]



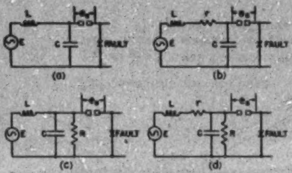


Figure 7. Single-frequency circuits

Circuit *c* is the most applicable to the general recovery-voltage problem. The developed equations are exact for this circuit. Circuit *c* is a good equivalent of circuits *b* and *d* for recovery-voltage analysis (See equation 2)

hence $-i_o$ in equation 5 is positive. For the great majority of cases e_{B_1} will also be negative with respect to E_1, permitting $(E_1-e_{B_1})$ to be numerically greater than E_1. For leading phase-angle interruptions[1] of zero-power-factor lagging circuits, E_1 also becomes negative.

The special case of vertical cutoff (equation 5—Figure 2) can be further simplified by expressing i_o in terms of the angle ψ and the slope of the current $(E_1-e_{B_1})/L$. With all angles expressed in radians on the base of 2π radians being the period of the undamped natural frequency, it follows that

$$-i_o=\frac{-\psi}{\omega_o}\left(\frac{E_1-e_{B_1}}{L}\right)$$

and equation 5 reduces to the expression found in equation 10.

In the general problem of current diversion as pictured in Figures 3 and 4, two triangular cancellation currents are encountered which, unlike the special case, are injected an interval *JO* or α degrees apart. This general case is solved in Appendix B and reduces to the compact form of equation 12. Equations 6 through 13, inclusive, describe the evolution of the single-frequency recovery-voltage equations. If one starts with the simplest case (equation 6) of normal current zero in a symmetrical zero-power-factor circuit, having no arc voltage, no resistance damping (Figure 7a), and without current diversion, equations are presented which successively introduce the various actions of the circuit interrupter and the circuit upon the final recovery-voltage characteristic.

Equation 12 is the most general single-frequency recovery-voltage equation here developed. It includes

1. Displacement.
2. Arc voltage.
3. Resistance damping.
4. Linear current diversion of which vertical cutoff is a special case, compactly expressed by equation 10.

When $\alpha=\psi$, equation 12 reduces to equation 9. The general equations 12 and 13 are only valid for values of time exceeding α/ω, that is, following the introduction of the second cancellation current. For values of ωt less than α the recovery voltage is given by the expression

$$e=[k_3e_{B_1}+k_2\text{ (equation 9)}]$$

The maximum voltage of all the undamped recovery-voltage expressions occur when the total angle is 270 degrees or $3\pi/2$ radians. At this point the sine has a value of minus one, and the maximum voltage of equation 13, for example, becomes

$$e_m=E_1+K_o(E_1-e_{B_1})$$

The function K_o therefore is a measure of the transient crest voltage. This coefficient is plotted in Figure 9 as a function of α. When α is zero, K_o terminates in M_o which is also shown plotted in Figure 9 as a function of ψ. The crest value of voltage for the special case of vertical cutoff becomes

$$e_m=E_1+M_o(E_1-e_{B_1})$$

When no arc voltage is present ($e_B=0$) and $E_1=E_m$, this reduces to

$$e_m=E_m(1+M_o)=E_m(1+\sqrt{1+\psi^2})$$

This shows the maximum recovery transient to be a function of the angle ψ, the value of cutoff current (equation 5) being of secondary importance except to indicate that ψ is usually largest when the current is small. Small, highly inductive currents are found in the magnetizing current of transformers, particularly when energized from the high-voltage side. The additional internal reactance of furnace transformers, together with a breaker close to the terminals (small *C*, high natural frequency) which is capable of sudden cutoff of light currents, makes this application one of the most vulnerable to overvoltages.[8,9] No small part of the hazard of this application is the frequent interruption of magnetizing current. The solution has been to decrease ψ, and hence the overvoltage, by increasing the period of the natural frequency of the circuit by the addition of shunt capacitors.[8,9]

When no current diversion is considered, then $K_o=M_o=1.0$, and the crest voltage is expressed as $(2E_1-e_{B_1})$. This function has been previously determined[1] and termed e_r. Curves of e_r as a function of arc voltage and other circuit parameters are shown in reference 1.

Figure 8. Typical triangle systems describing the geometric relations between the slope coefficients (k_2 and k_3) and the undamped pendulum coefficient K_o (Equation 13)

These triangles both fall in a spiral generated as α is varied

(a). The high-rate triangle (b). The low-rate triangle

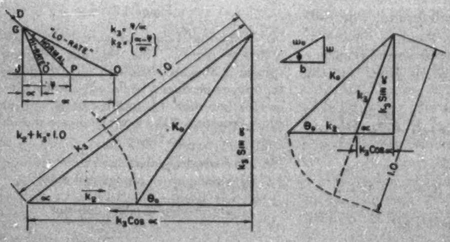
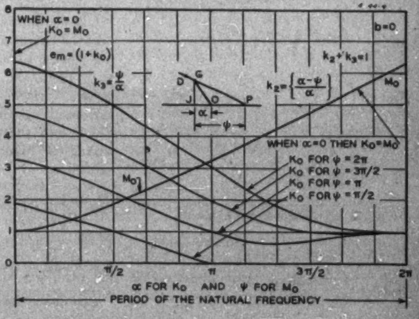


Figure 9. The undamped pendulum coefficient K_o as a function of the diversion angles α and ψ

When $\alpha=0$, then $K_o=M_o$, corresponding to vertical cutoff. See equations 13 and 11

$$K_o=\sqrt{1-2k_2k_3(1-\cos\alpha)}$$

$$M_o=\sqrt{1+\psi^2}=(K_o\text{ for }\alpha=0)$$



SINGLE-FREQUENCY RECOVERY-VOLTAGE EQUATIONS

Normal current-zero interruption, no damping, no arc voltage, symmetrical case. Used for calculating circuit recovery rates:

$$e = E_m(1 - \cos \omega_o t) \qquad (6)$$

(For nomenclature see page 386.)
Same, except general asymmetrical case

$$e = E_1(1 - \cos \omega_o t) \qquad (7)$$

Including arc voltage

$$e = E_1 - (E_1 - e_{B_1}) \cos \omega_o t \qquad (8)$$

With resistance damping added (Figures 7b, c, and d)

$$e = E_1 - (E_1 - e_{B_1}) \cdot \frac{\omega_o}{\omega} \cdot \epsilon^{-bt} \sin (\omega t + \phi) \qquad (9)$$

Including sudden cutoff, ψ degrees ahead of normal zero (Figure 2)

$$e = E_1 - (E_1 - e_{B_1}) \cdot M \cdot \epsilon^{-bt} \sin (\omega t + \delta) \qquad (10)$$

Cutoff, but no resistance damping

$$e = E_1 - (E_1 - e_{B_1}) \cdot M_o \cdot \sin (\omega_o t + \delta_o) \qquad (11)$$

General case, everything including linear current diversion

$$e = E_1 - (E_1 - e_{B_1}) \cdot K_b \cdot \epsilon^{-bt} \cdot \sin (\omega t + \phi - \theta_b) \quad \text{for } t > (\alpha/\omega) \qquad (12)$$

General case, current diversion but no damping

$$e = E_1 - (E_1 - e_{B_1}) K_o \cdot \sin \left(\omega_o t + \frac{\pi}{2} - \theta_o \right) \quad \text{for } t > \frac{\alpha}{\omega_o} \qquad (13)$$

Where, in the preceding equations

$$\omega_o = \frac{1}{\sqrt{LC}} \qquad b = \frac{r^*}{2L} \text{ (Figure 7b)}$$

$$b = \frac{1}{2RC} \text{ (Figure 7c)} \qquad \omega = \sqrt{\omega_o^2 - b^2}$$

$$\text{Tan } \phi = \frac{\omega}{b} \qquad B = \left(\frac{b}{\omega_o} + \psi \right)$$

$$k_3 = \frac{\psi}{\alpha} \qquad M = \frac{\omega_o}{\omega} \sqrt{B^2 + \left(\frac{\omega}{\omega_o} \right)^2}$$

$$\text{Tan } \delta = \left(\frac{\omega}{\omega_o B} \right) \qquad k_4 = \epsilon^{\frac{b\alpha}{\omega}} \qquad k_2 = \frac{\alpha - \psi}{\alpha}$$

$$M_o = \sqrt{1 + \psi^2} \qquad \text{Tan } \delta_o = \left(\frac{1}{\psi} \right)$$

$$(k_2 + k_3) = 1.0$$

$$K_b = \frac{\omega_o}{\omega} \sqrt{k_2^2 + 2k_2k_3k_4 \cos \alpha + (k_3k_4)^2}$$

$$K_o = \sqrt{1 - 2k_2k_3(1 - \cos \alpha)}$$

$$\text{Tan } \theta_b = \left\{ \frac{k_3k_4 \sin \alpha}{k_2 + k_3k_4 \cos \alpha} \right\}$$

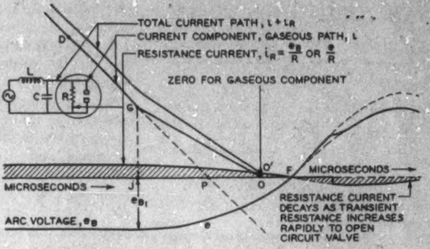

TOTAL CURRENT PATH, $i + i_R$
CURRENT COMPONENT, GASEOUS PATH, i
RESISTANCE CURRENT, $i_R = \frac{e_B}{R}$ OR $\frac{e}{R}$
ZERO FOR GASEOUS COMPONENT
D
L
C
R
G
O′
F
MICROSECONDS
MICROSECONDS
J
P
O
e_{B_1}
ARC VOLTAGE, e_B
e
RESISTANCE CURRENT DECAYS AS TRANSIENT RESISTANCE INCREASES RAPIDLY TO OPEN CIRCUIT VALVE

Figure 10. Interpretation of the current-zero phenomena in an interrupter which has or develops a transient leakage resistance *R* in shunt with the arc gap

The division of arc-gap current into two components sheds considerable light on the mechanism of the low-rate case

$$\text{Tan } \theta_o = \left\{ \frac{k_3 \sin \alpha}{k_2 + k_3 \cos \alpha} \right\}$$

OTHER TYPES OF CURRENT-ZERO PHENOMENA

Figure 5 shows several other types of current-zero phenomena which may be fabricated by the use of the rectangular and triangular cancellation currents. The special case of vertical cutoff as shown in Figure 2, together with the resulting recovery-voltage (equation 10), may be utilized in a new and more general manner by reintroducing at the sudden interruption, any new type of nonlinear current path desired and adding the resulting recovery voltage to equation 10. One assumption which probably comes closer than the linear diversions assumed by the rectangular and triangular components is the exponential decay. This case is shown by the family of curves in Figure 11 and covers a host of current-zero variations. Although more complicated than the linear components it is probably the easiest of the nonlinear current variations from an analytical viewpoint.

The solution of this case will only be indicated here. If at the point G, Figure 2, the current decays exponentially from i_o according to the equation

$$i_o \epsilon^{-\lambda t}$$

it follows that by introducing this current into the circuit and by adding the resulting voltage to the case of vertical cutoff at i_o (equation 10), the true recovery-voltage variations will result. The voltage resulting from the introduction of $i_o \epsilon^{-\lambda t}$ into the general single-frequency circuit of Figure 7c is given by the expression

$$e_\lambda = \frac{i_o}{\omega C} \left\{ W_o \epsilon^{-bt} \sin (\omega t + \gamma) - \frac{Y\omega}{S} \epsilon^{-\lambda t} \right\} \qquad (14)$$

where

$$N = (b - \lambda) \qquad S = \sqrt{N^2 + \omega^2} \qquad Y = \frac{\lambda}{S}$$

$$W_o = \sqrt{1 + 2Y \cos \Omega + Y^2}$$

$$\text{Tan } \Omega = \omega / N$$

$$\text{Tan } \gamma = \left\{ \frac{Y \sin \Omega}{1 + Y \cos \Omega} \right\} = \left\{ \frac{\lambda \omega}{S^2 + \lambda N} \right\}$$

When $\lambda = 0$, this reduces to the e' of equation 3. The net recovery voltage as a result of an exponential decay ($i_o \epsilon^{-\lambda t}$) of current from i_o, ψ degrees ahead of normal current zero, is given by the superposition equation

$$e = e_{B_1} + e' + e'' + e_\lambda$$

The ultimate recovery voltage for this case becomes

$$e = E_1 - (E_1 - e_{B_1}) \frac{\omega}{\omega_o} \left\{ A \epsilon^{-bt} \sin (\omega t + \Gamma) + J \epsilon^{-\lambda t} \right\} \qquad (15)$$

where

$$A = \sqrt{(B - H)^2 + \left(\frac{\omega}{\omega_o} - J \right)^2}$$

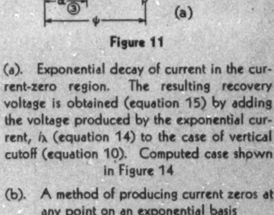


Figure 11

(a). Exponential decay of current in the current-zero region. The resulting recovery voltage is obtained (equation 15) by adding the voltage produced by the exponential current, i_λ (equation 14) to the case of vertical cutoff (equation 10). Computed case shown in Figure 14

(b). A method of producing current zeros at any point on an exponential basis

$$\lambda = \frac{\omega_o}{\alpha} \text{ when } \alpha = \text{time constant}$$

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*Approximate—see Appendix A.

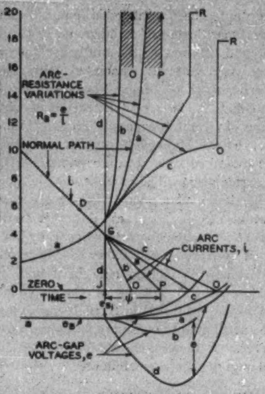


Figure 12. Arc-gap-resistance variations in the current-zero region as defined by the ratio of recovery voltage, *e*, to the corresponding arc-gap current, *i*

High-rate phenomena tend to produce arc-gap resistances which approach infinity at actual current zero. Low-rate phenomena produce arc-gap-resistance variations which indicate a finite arc-gap resistance at current zero. Figure 10 sheds additional light on the mechanism of low-rate phenomena

$$\operatorname{Tan}\Gamma=\frac{\left(\frac{\omega}{\omega_0}-J\right)}{(B-H)}$$

$$B=\left(\frac{b}{\omega_0}+\psi\right);\quad J=\psi\left(\frac{\omega Y}{S}\right);\quad H=\psi\left(1+\frac{YN}{S}\right)$$

The recovery voltage resulting from the exponential decay (equation 15) is plotted as one of the cases shown in Figure 14. When the time constant of the exponential decay is α (radians), then $\lambda=\omega_0/\alpha$. In equation 15, as in equations 10 through 13, the angle ψ is always positive. This indicates that i_0 of equation 5, which is the magnitude of the rectangular cancellation current, is included in all subsequent equations with a negative sign. This is also true of equation 15, and, in addition, the sign of the exponential component has been considered as positive. Thus in equation 15 when $t=0$, $e=e_{B_1}$; when $\lambda=0$, equation 9 results. When $\lambda=\infty$, equation 10 results, and so forth.

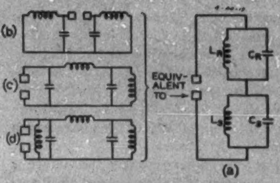


Figure 13. Some typical recovery-voltage circuits having two natural frequencies

All are equivalent to circuit a.[3] Cancellation current components produce recovery-voltage components in each of the circuits as dictated by the natural frequencies and in amplitude proportional to the respective inductances. Damping of these oscillations is important

The exponential solution of equation 14 may also be used to fabricate various nonlinear current-zero phenomena. By permitting both the rectangular cancellation current $-i_0$ and the initial magnitude of the exponential current i_0 to be increased to i_0' (see Figure 11b), current zeros may be obtained at any desired point on an "exponential" basis. The balance of the negative current which would approach (i_0-i_0') may be cancelled by adding at $i=0$ the cancellation current $(i_0'-i_0)(1-\epsilon^{-\lambda t})$. This component could be neglected in the majority of cases. In some cases slight negative currents are representative of true conditions and represent glow states within the interrupter. Under such conditions the recovery-voltage transient must pass through zero with the current in order to avoid an ambiguity in the concept of arc-gap resistance.

ARC-GAP RESISTANCE

Although the solutions presented here consider the resistance R to shunt the arc, it is not intended that this resistance necessarily be considered as the arc resistance. Arc-gap resistance is a by-product of this analysis. It is defined as the ratio of arc-gap voltage to arc-gap current and at all times must remain a positive quantity. Inasmuch as the recovery voltage e, as developed in the relations presented, is initiated at the point of current diversion G and prior to current zero, it follows that arc-gap-resistance variations are available for the entire period by expressing the ratio of arc-gap voltage e to the corresponding instantaneous current i.

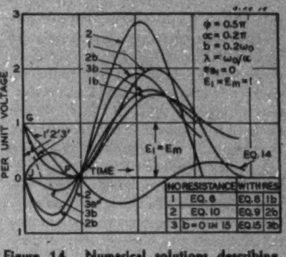


Figure 14. Numerical solutions describing the nature of various recovery transients as influenced by current-zero phenomena and damping

Curve 1—Normal current zero, no damping

Curve 1*b*—Normal current zero, with damping, $b=0.2\omega_0$

Curve 2—Vertical cutoff, $\psi=0.5\pi$ (90 degrees. No damping

Curve 2*b*—Vertical cutoff, with damping. $b=0.2\omega_0$

Curve 3*a*—Exponential current-zero phenomena. Voltage resulting from injection of cancellation component $i_0\epsilon^{-\lambda t}$ (equation 14, Figure 11)

Curve 3*b*—Total recovery voltage as a result of exponential decay, including damping. $3b=3a+2b$ (equation 15)

Consider the case where the current proceeds normally to zero along the path *DGP* in the presence of a constant arc voltage e_B. The arc resistance is then the reciprocal of the current $(R_a=e_B/i)$ and will approach infinity as the current approaches zero. This resistance variation is shown as curve *a* in Figure 12. In spite of this distinctly nonlinear resistance characteristic the solution of the arcing period[1] was accomplished by linear equations and did not require the direct consideration of this nonlinear resistance parameter.

When the current is diverted to approach zero at a faster rate than normal (high-rate case Figure 12) the initial recovery voltage exhibits a negative dip contributed by the rectangular cancellation component, which, together with the more rapid decay of current, gives rise to a resistance characteristic which departs from *a*, Figure 12 and follows curve *b*. Again the resistance is permitted to approach an infinite value at current zero. For this reason the recovery voltage inherently cannot pass through zero until after the current has reached zero. Interrupters which "force" the current zero are capable of developing rapidly increasing arc-gap resistances; the resulting overvoltages, however, place an additional stress on the arc gap which sometimes tends to offset the apparent gain in dielectric strength.

When the current is diverted to approach zero at a slower rate than normal (low rate of Figure 12) both triangular cancellation currents are positive (Figure 4) which permits the arc-gap voltage e to depart from the arc voltage e_{B_1} and bend immediately toward the voltage axis. This decreasing arc-gap voltage, together with the higher per-unit arc current, define an arc-resistance variation which follows curve *C* of Figure 12. This characteristic indicates that a finite resistance is being approached as the current approaches zero. If this current is assumed to become entirely extinguished at current zero in the presence of a finite arc-gap voltage, then the arc-gap resistance becomes discontinuous at current zero, jumping suddenly to infinity. Under certain conditions this is a practical assumption; however, an artifice more in keeping with the physics of the phenomena would permit the arc-gap resistance to become discontinuous at current equal zero but to approach a new high finite resistance level.

This practical condition may be considered within the boundaries of the developed equations without introducing new initial conditions at the instant of current zero; namely, by the expedient of permitting this high arc-gap resistance to be present throughout the current-zero period. This resistance may therefore be represented completely, or in part, by the parameter *R* but considered as part of the arc gap. Figure 10 describes these considerations. Here the arc-gap current is considered divided into two parts, the gaseous component i and the resistance component (i_R). The gaseous current component is assumed to follow the line *DGO* (Figure 10), as decribed in this paper for the circuit of Figure 7c. The resistance current component is then the resulting recovery voltage (equation 12—low-rate case) divided by the resistance *R*. The true arc-gap current is then the sum of the two currents. The reason for the new diversion of total arc-gap current at *O′* to *F* is clear and recapitulates the basic cause of the initial diversion at *G*. A still clearer insight into the basic mechanics of the low-rate case may be obtained by adding to the current i (*DGP*), which produces the voltage e of equation 9 (resistance and no diversion), the resistance current e/R. The arc-gap resistance R_a then becomes

$$R_a=\left\{\frac{e_{B_1}R}{e_{B_1}+R\cdot i}\right\}\text{ or }\left\{\frac{eR}{e+R\cdot i}\right\} \qquad (16)$$

and is equal to *R* at $i=0$. The approximation of the true nature of current diversion seems strengthened when compared to the preceding case.

At the outset, the nature of the arc-gap resistance following current zero should be made clear. Since this analysis is dealing with phenomena which take place on a microsecond scale, it is a fairly safe approximation to consider this arc-gap resistance to remain constant for the interval from current zero through the first positive recovery-voltage crest. Actually this resistance increases, probably exponentially, to the open-circuit insulation resistance in a matter of a few hundred microseconds. It is a purely transient parameter, and in no way should it be interpreted as permitting a permanent current to flow across the interrupter following current zero.

The preceding concept of permitting the current within the interrupter to be divided between the main arc current in the gaseous medium and the current through the transient leakage paths within the interrupter, during and immediately following interruption, has been of considerable aid in analytically interpreting the cathode-ray oscillograms depicting the behavior of a wide variety of commercial designs. The degree to which the beneficial action of a controlled transient leakage path within an interrupter has been mastered is demonstrated by the cathode-ray oscillograms (Figures 18 and 19) showing the mildly oscillatory recovery voltages associated with the performance of Magne-Blast air circuit breakers. The recent extension of this line of circuit breakers to include 15-kv service and higher interrupting capacities is described in a separate paper.[10] Its consideration will shed additional light on the design of a modern air interrupter which incorporates these desirable transient-resistance characteristics in its normal performance. Other oscillograms showing this action may be found in reference 1. Not all of the apparent damping exhibited in these records may be attributed to resistance damping, however. A more complete description of damping phenomena follows.

Damping of Recovery-Voltage Oscillations

One of the most interesting observations to be made in the analytical treatment presented here is the manner in which the nonlinear portion of the arc-gap resistance (prior to current zero) has its effect upon the magnitude of the recovery oscillation. Those interruptions in the low-rate region (Figure 1) which permit the arc-gap resistance to approach a finite resistance at current zero (Figure 12) give rise to two similar recovery-voltage components displaced by α degrees. Theoretically, if this interval of separation, α, represented 180 degrees of the natural period, then the recovery-voltage components would be completely out of phase, and their addition, following $\omega t=\alpha$, would appear as a smooth curve with no trace of the oscillatory nature of the circuit. A casual inspection of such a condition would give the impression of enormous damping.

Figure 15a. Numerical solution describing the influence of the diversion angle α on the magnitude and position of the crest value, e_m, of the recovery transient

No resistance damping included. The dotted region implies improbable conditions and shows that low-rate phenomena are more probable under conditions of high arc voltage[10]

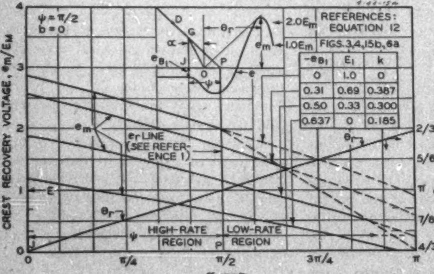
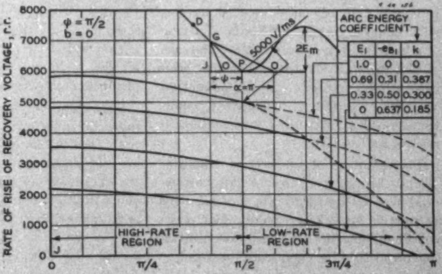


Figure 15b. Corresponding influence on the rate of rise of recovery voltage

No resistance damping



A natural, but erroneous, conclusion would be that a low resistance path was present following current zero. This limiting case is, however, impossible of attainment. It is clear, however, that this condition can be partially approached and still permit the arc-gap resistance following current zero to be expressed in megohms.

Apparent damping of the recovery oscillations may, therefore, be divided into two separable components

1. The neutralizing action of the two recovery-voltage components which arises as a result of their out-of-phase relationship.
2. The normal resistance damping of both of the recovery-voltage components as a result of the resistance R which shunts the interrupter both before and immediately following current zero.

The nature of the resistance R is discussed more fully in the previous section under the heading of "Arc-Gap Resistance." The effect of circuits having two natural frequencies introduces an additional component peculiar to this problem and is discussed under that heading.

CIRCUITS HAVING TWO NATURAL PERIODS

It has been shown[2] that all the practical recovery-voltage circuits encountered in the field which have two natural frequencies may be reduced to the equivalent circuit shown in Figure 13a. It follows that any of the cancellation currents previously considered, when applied to this circuit, will produce voltage oscillations across each of the circuits as indicated by the equations developed in this paper. Cathode-ray oscillograms of interruptions which take place in the high-rate region (Figure 1) will exhibit both frequencies in the recovery-voltage oscillation, the higher frequency being the more highly damped. The magnitude of the respective oscillations is proportional to the voltages associated with the inductances of this circuit.[2] When the air interrupters of the magnetic type are tested on the same circuits, however, the low-rate interruptions permit the leakage resistance R to be developed prior to current zero as described. The exact method of dividing this resistance effectively across the two circuits of Figure 13a is not known; nevertheless, the result, invariably, is to find the high-frequency component so highly damped as not to be discernible on the cathode-ray trace. This damping may arise as a result of several actions, namely

(*a*). Nonoscillatory relations.

(*b*). The displacement angle between the two cancellation components α which can more readily permit the two high-frequency components to neutralize.

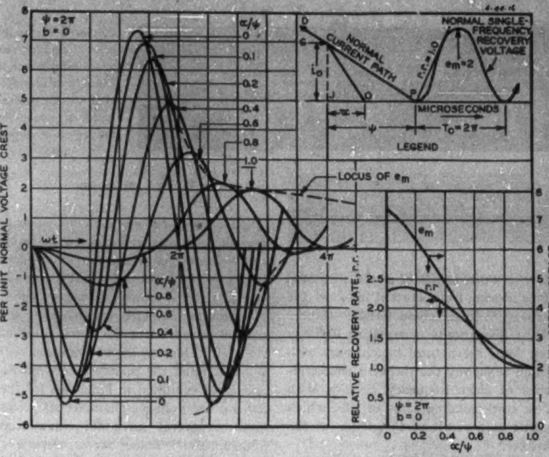


Figure 16. Numerical solutions describing the evolution of the recovery-voltage transients as a function of the diversion angle α, for a value of ψ equal to the period of the natural frequency

No resistance damping is included. Disruptive voltages can be produced when ψ is large and α small

In other words, the aforementioned high-frequency component in the recovery oscillation is absorbed within the resistance changes which take place between current diversion and current zero, and in addition this region is capable of imparting considerable (apparent) damping to the low-frequency component of the recovery oscillation as previously described. The loss of the high-frequency component f_S leaves but one frequency f_R to appear on the recovery-voltage trace, and this in turn is limited in magnitude (exclusive of damping) to the voltage associated with the inductance L_R shown in Figure 13a. As a result the magnitude of the remaining single-frequency oscillation in circuits having two natural frequencies is inherently lower than had the high-frequency circuit not been present. This fact, in the majority of cases, permits the double-frequency circuits of Figure 13 to be slightly less severe for low-rate interrupters while tending to be more severe for the high-rate interrupters.

APPLICATION TO D-C INTERRUPTIONS

The interruption of d-c solenoids, trip coils, field circuits, and so forth, may be analyzed quite readily by the use of the triangular cancellation currents alone. The actions are, in general, so slow as to be able to neglect the inherent capacitances of the circuit. When capacitors are present across the switch, the solutions in this paper are valid. For the circuit without capacitance considered, it can be shown by cancellation principles that the maximum voltage which appears across the solenoid is

$$e_m = \frac{ERL}{(R+r)rt_1}\left\{1 - e^{-\left(\frac{R+r}{L}\right)t_1}\right\} \qquad (17)$$

where

E = normal d-c voltage
R = resistance shunting the coil, ohms
L = inductance of coil circuit, henrys[18]
r = resistance of coil circuit, ohms
t_1 = time in seconds for the switch to bring the steady-state d-c current linearly to zero

A plot of this relation will show the importance of t_1 in relation to R. For vertical cutoff interruptions of d-c circuits the inherent capacitances cannot be neglected.

INFLUENCE UPON RECOVERY RATES

For application purposes the rate of rise of recovery voltage, as determined, is a circuit constant and is independent of the action of the circuit breaker and is usually computed independent of any damping afforded by the series resistance r of the circuit. For single-frequency circuits, equation 6 expresses the recovery voltage in its simplest terms.

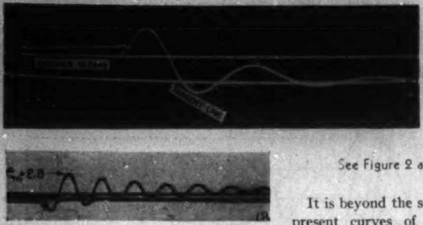


Figure 17. Cathode-ray oscillograms which exhibit typical characteristics associated with the vertical cutoff of current in the region of current zero

See Figure 2 and equation 10

Recovery rate is defined as the maximum slope drawn from current zero to the recovery-voltage characteristic (Figure 15b). The results of equation 6 therefore form the natural bench mark with which to survey the recovery characteristics of single-frequency circuits, with their corresponding rates of rise, which accrue as a result of the action of the breaker.

The ultimate recovery voltage experienced by a circuit breaker may be broken roughly into four parts:

1. The influence of the circuit constants and connections including the damping factors offered by series and shunt resistance exclusive of the breaker. These may be termed the static factors.

2. The asymmetry of the fault current experienced by the breaker with the parting of contacts. This is a random phenomenon and is capable of influencing considerably the events which take place within the arcing period. In this section should be included the random angle between the current zero prior to arcing and the parting of contacts. These may be termed the random factors.

3. The arcing period is capable of playing a tremendous part in influencing the recovery period.[1] It determines the factors $E_1-(E_1-e_{B_1})$ in equations 8 through 13. Because of the random factors listed, together with the action of the breaker, any magnitude of arc voltage e_{B_1} (within design and circuit limitations) may occur with any value of E_1. The arcing period also is capable of so influencing the arc gap following interruption as to permit short-lived glow states to exist in the microsecond period following current zero. This in turn introduces an additional damping factor in the form of a shunt resistor R, which is present long enough to modify the first peak of the recovery transient.

4. The action of the breaker in permitting the current path to be modified in the region of current zero may be termed the true current-zero phenomena. It has a direct and positive effect upon the ultimate recovery characteristics. This action is not independent of the arcing period which in turn is somewhat dependent upon the random factors. The magnetic-type air circuit breakers appear to offer the greatest opportunity in controlling this current-zero action.[10,11]

It is beyond the scope of this paper to present curves of the many possible combinations of these variables. One such problem is presented in Figures 15a and b. Here is shown the case of current diversion taking place one quarter of the natural period before the normal current zero P. This solution (equation 12) is based upon the relations between e_{B_1} and E_1 as exhibited in Figure 6a for the flat-top arc voltages characteristic common to several successful commercial interrupters.[10] These curves assume a single-frequency-circuit rate of rise of 5,000 volts per microsecond as would be given by equation 6. Figure 15a shows the variations which take place in the maximum value of recovery-voltage transient as the current, pivoted at G, is permitted to swing from $\alpha=0$ to $\alpha=180$ degrees. The recovery-voltage angle θr is also plotted which enables the rate of rise curves shown in Figure 15b to be computed.

Figures 15a and b reveal not only the influence of the diversion angle α but show the effect of the flat-top arc voltages upon the crest recovery voltages and the corresponding rates of rise. The values of k shown on Figures 15a and b are the arc energies in watt hours per megavolt-ampere as a result of the flat-top arc voltages produced in a zero-power-factor circuit under symmetrical conditions.[1] The flat-top arc voltage of 31 per cent of the generated crest is the condition for maximum arc energy under these conditions.

No resistance damping is considered in the curves of Figure 15. Resistance which develops during and following interruption as a result of the presence of a short-lived glow discharge would cause these curves to fall still lower in the low-rate region. Interruptions in the high-rate region are not likely to experience the benefit of glow-discharge effects. The problem of resistance damping is discussed more fully under that heading.

One interesting aspect of Figure 15 is the time relation of actual current zero (from which recovery voltage is measured) backward to the energy developed during the previous arcing period. The high-rate interruptions are not only closer, in time, to the arcing period but impress a higher voltage across the gap. The low-rate interruptions, on the other hand, delay the current zero and in turn permit a lower recovery voltage and rate of rise to exist. This action tends to accentuate effectively the variations exhibited between the two regions. The arc-gap-energy characteristic during the diversion period is available from the equations presented in this paper but is beyond the scope of this presentation.

OTHER APPLICATIONS

Brief mention has been made of the mechanism of the generation of overvoltages by the too rapid extinction of the current prior to normal current zero.[7] (See Figure 16.) This phenomenon is sometimes encountered with the interruption of small, highly inductive currents by interrupters of the fluid type. The conditions which bring this about become accumulative as the inductive current is decreased, namely:

1. Small currents are more easily cooled and hence more likely to become unstable before a natural current zero.

2. Higher natural-frequency circuit is associated with smaller magnetizing inductance.

3. The angle ψ increases for a given instantaneous cut-off current.

These conditions are further accentuated in the arc-furnace[8,9] application on the following counts:

1. Lower than normal magnetizing current when supplied from the high side.

2. Close proximity of the breaker to the transformer which further increases the natural frequency due to the small inherent capacitance to neutral.

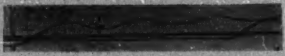
3. Frequent interruption of magnetizing current.

Two solutions are available:

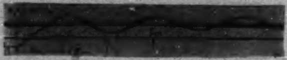
1. Decrease the natural frequency (smaller ψ) by the addition of shunt capacitors in parallel with the transformer.[8,9]

2. Employ an air interrupter which is capable of interrupting magnetizing current efficiently in the low-rate region, thus avoiding the primary cause of the overvoltage.[10]

One of the most practical applications of this work is the opportunity it affords to predetermine the relative severity of various circuit applications for a given interrupter, its performance characteristics being available on a known set of circuit constants. The relative severity of various circuits will be different depending upon the current-zero characteristics of the interrupter. For interrupters which exhibit low-rate phenomena, rela-



(a). Single-frequency circuit phenomena. Compare with the analysis shown in Figure 10



(b). Low-rate phenomena in a circuit having two natural frequencies, the higher of which is "shock-excited" in a manner similar to (a) and described in Figure 10

Figure 18. Cathode-ray traces which exhibit phenomena in the low-rate region and which are accompanied by evidence of transient-resistance characteristics

tive circuit severities all but disappear leaving the circuit voltage and short-circuit kilovolt-amperes as being the major considerations.

On a development-design basis, the variations of the current-zero characteristics can be traced and catalogued with respect to the arc-chute-design changes. In this manner the factors which contribute to the most desirable current-zero characteristics can be determined and incorporated in the completed design.[10]

Summary

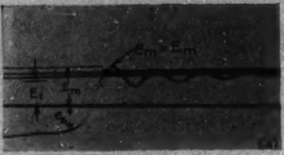
1. A straightforward method of determining the interrelation of recovery-voltage, arc-current, and arc-resistance variations has been presented.

2. A study of the complete pendulum action of the current in the region of current zero has permitted a keener insight into generation of the complete family of recovery-voltage characteristics associated with both oil and air circuit breakers.

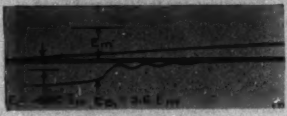
3. Interrupters which exhibit large arc voltages over the entire arcing period experience reduced recovery-voltage oscillations and have the additional advantage of operating in a region which permits the current to be diverted and approach zero at a rate slower than normal.

4. Conversely, interrupters which develop low arc voltages are, in general, capable of producing greater than normal rates of change of current in the current-zero region. Hence, under conditions of small inductive currents in circuits of high natural frequencies, this action leads to overvoltage conditions which must be considered in their application.

5. The concept of dividing the arc current into parallel paths, the gaseous medium and the transient leakage path, has not only permitted the determination of the exact nature of interrelated variations of current and voltage but also gives a clearer insight into the mechanism of circuit interruption.



(a). Interruption which occurs with $e_{B_1}=0.6$ and $E_1=0.7$



(b). Cathode-ray trace of a leading phase-angle interruption of a zero-power-factor short circuit. Low-rate phenomenon is exhibited in which $e_{B_1}=0.60$ and E_1 is negative at -0.25

Figure 19. Typical low-rate phenomena associated with the performance of the Magne-Blast air circuit breaker[10]

6. Apparent damping of the recovery oscillations has been shown to be due not entirely to resistance effects but to a combination of resistance, heterodyning action (out-of-phase relationships) of the recovery-voltage components, and the early loss of the high-frequency component in circuits having two natural frequencies.

7. The method of attack lends itself to the study of any type of current-zero phenomena in any circuit involving the a-c arc.

8. Circuit interrupters which employ the beneficial aspects of a controlled current-zero phenomenon have been indicated.

Appendix A

Solution of the Voltages Resulting From the Introduction of the Rectangular (i_o) and the Triangular $(E_1-e_B)t/L$ Cancellation Currents in the Circuit of Figure 7c

The generated voltage is assumed constant at E_1 during the current-zero interval. The presence of arc voltage (e_B) in a zero-power factor circuit permits the current to have the slope $(E_1-e_{B_1})/L$ in the current-zero region, where e_{B_1} is the instantaneous arc voltage just prior to current zero.[1] Hence, the voltage e_{B_1} is one of the terms of the recovery voltage and is the contribution of the circuit current. The cancellation currents are always introduced "through the breaker" and hence encounter the parameters of R, L, and C (Figure 7c) in parallel. As dictated by superposition principles, all other sources of current are considered removed but retain their through impedances. In this light, the parameter L (Figure 7c) always includes the inductance of the primary source of current. Since we are dealing with phenomena in the region of current zero, the L parameter may be considered constant as those portions of the circuit containing iron will not be influenced by saturation.[13]

Since the parallel impedance of Z_1, Z_2, and Z_3 is

$$\left\{\frac{Z_1\cdot Z_2\cdot Z_3}{Z_1Z_2+Z_1Z_3+Z_2Z_3}\right\} \tag{21}$$

the operational impedance of Figure 7c, viewed from the breaker, may be obtained by substituting

$$Z_1=R;\quad Z_2=\frac{1}{Cp};\quad \text{and } Z_3=Lp$$

Performing this operation and reducing, we have

$$Z(p)=\left\{\frac{\dfrac{p}{C}}{p^2+\dfrac{p}{RC}+\dfrac{1}{LC}}\right\}1 \tag{22}$$

The voltage e' resulting from the rectangular cancellation current $i_o\cdot 1$ is therefore

$$e'=Z(p)\cdot i_o 1$$

which becomes

$$e'=\frac{i_o}{C}\left\{\frac{p}{p^2+2bp+\omega_o^2}\right\}1 \tag{23}$$

where

$$b=1/2RC \text{ and } \omega_o^2=1/LC$$

From a table of operational relationships,[5] however

$$\left\{\frac{p}{p^2+2bp+\omega_o^2}\right\}1=\frac{\epsilon^{-bt}}{\omega}\sin\omega t \tag{24}$$

where $\omega^2=(\omega_o^2-b^2)$ and remains positive. It follows therefore that e' becomes

$$e'=\frac{i_o}{\omega C}\epsilon^{-bt}\sin\omega t \tag{25}$$

The voltage e'' resulting from the injection of the triangular cancellation current $i(t)=(E_1-e_{B_1})t/L$ (see text) becomes

$$e''=Z(p)i(t)1=(E_1-e_{B_1})\left\{\frac{\omega_o^2}{p^2+2bp+\omega_o^2}\right\} \tag{26}$$

This is made possible by the operational formula

$$\frac{t^m}{m!}=\frac{1}{p^m} \tag{27}$$

From the operational relationship

$$\left\{\frac{\omega_o^2}{p^2+2bp+\omega^2}\right\}1= \left[1-\frac{\omega_o}{\omega}\epsilon^{-bt}\sin(\omega t+\phi)\right] \tag{28}$$

where again $\omega^2=(\omega_o^2-b^2)$ and remains positive and $\tan\phi=\omega/b$, it follows that

$$e''=(E_1-e_{B_1})\left\{1-\frac{\omega_o}{\omega}\epsilon^{-bt}\sin(\omega t+\phi)\right\} \tag{29}$$

Both e' and e'' are zero for $t=0$. The aforementioned relationships hold for Figure 7a when $R=\infty$ (or $b=0$) and $\omega=\omega_o$
e' then becomes

$$i_o\sqrt{L/C}\sin\omega_o t$$

and

$$e''=(E_1-e_{B_1})\{1-\cos\omega_o t\} \qquad (30)$$

Relationship of equations 25 and 29 are accurate to a high degree for the circuits of Figure 7b by permitting $b=r/2L$ and $\omega^2=(\omega_o^2-b^2)$. This procedure neglects a term similar to equation 28 whose coefficient is $i_o r$. This term is negligible compared to the master term whose coefficient is $i_o/\omega C$. This is equivalent to considering r in relation to $\sqrt{L/C}$. For the triangular cancellation current the error is smaller since the initial slope is truly $(E_1-e_{B_1}-i_o r)/L$ and tends to neutralize the error referred to. It can be assured that these errors are negligible in comparison to the natural assumptions made in a study of this type.

The circuit of Figure 7d may be considered by converting r to R of Figure 7c as described below equation 2. This is subject to the same negligible limitations.

Appendix B

Development of Compact Expressions for the Recovery Characteristics as Exhibited by the Cancellation Equations of Figures 2, 3, and 4

A study of Figure 2 together with Appendix A reveals that the recovery-voltage characteristic for the case of sudden cutoff becomes

$$e=e_{B_1}+e'+e''=(e_{B_1}+\text{equation } 25+\text{equation } 29) \qquad (31)$$

Making this addition as dictated by Figure 2, we have

$$e=E_1-\epsilon^{-bt}\left\{(E_1-e_{B_1})\cos\omega t+\frac{1}{\omega C}\left[-i_o+\frac{E_1-e_{B_1}}{2R}\right]\sin\omega t\right\} \qquad (32)$$

The use of such an expression becomes of more practical value when expressed in terms of the natural period of the circuit. The base chosen is the period, T_o, of the natural angular velocity ω_o. An inspection of Figure 2 will show that when ψ is measured in seconds

$$-\left\{\frac{E_1-e_{B_1}}{L}\right\}=\frac{i_o}{\psi} \qquad (33)$$

When ψ is measured in radians

$$-i_o=\left\{\frac{E_1-e_{B_1}}{L}\cdot\frac{\psi}{\omega_o}\right\} \qquad (34)$$

When this substitution is made in equation 32

$$e=\{E_1-(E_1-e_{B_1})\epsilon^{-bt}\cdot M\cdot\sin(\omega t+\delta)\}$$

$$B=\left(\frac{b}{\omega_o}+\psi\right) \qquad (35)$$

$$M=\frac{\omega_o}{\omega}\sqrt{B^2+\left(\frac{\omega}{\omega_o}\right)^2}\text{ and }\tan\delta=\left(\frac{\omega}{\omega_o B}\right) \qquad (36)$$

When $\psi=0$, no vertical cutoff occurs, and $B=b/\omega_o$; $M=\omega_o/\omega$ and $\delta=\phi$ and equation 35 reduces to $e_{B_1}+e''$ (see equation 29).

When $E_1=E_m$, $e_{B_1}=0$, $b=0$, and $\psi=2\pi$; then $e_m=1+\sqrt{1+4\pi^2}=7.35$ or 7.35 times normal crest. If ψ were reduced to 0.4π by increasing the capacitance 25 fold, then $e_m=1+\sqrt{1+4\pi^2\times0.04}=2.61$

Development of Recovery-Voltage Equations for the General Case of Current Diversion in a Single-Frequency Circuit of Figure 7c

For the general case of current diversion, requiring the introduction of two triangular cancellation components (Figures 3 and 4) having respectively the slopes k_2 and k_3 times the initial current slope, the latter being introduced α radians following the first, the expression for e following $\omega t=\alpha$ may be expressed as

$$e=e_{B_1}+k_2e''(t)+k_3e''(t-\alpha) \qquad (38)$$

From plane geometry it can be shown that

$$k_3=\psi/\alpha \qquad (39)$$

$$k_2=\left\{\frac{\alpha-\psi}{\alpha}\right\} \qquad (40)$$

and

$$k_2+k_3=1.0 \qquad (41)$$

Making all substitutions in equation 38 and reducing, we have

$$e=E_1-(E_1-e_{B_1})\epsilon^{-bt}\cdot\frac{\omega_o}{\omega}\cdot\{k_2\sin(\omega t+\phi)+k_3k_4\sin(\omega t+\phi-\alpha)\} \qquad (42)$$

where

$$k_4=\epsilon^{b\alpha/\omega}$$

Reducing further by trigonometric substitution, we have

$$e=E_1-(E_1-e_{B_1})\cdot K_b\cdot\epsilon^{-bt}\times\sin(\omega t+\phi-\theta_b)\text{ for }t>\frac{\alpha}{\omega} \qquad (43)$$

where

$$K_b=\frac{\omega_o}{\omega}\sqrt{k_2^2+2k_2\cdot k_3\cdot k_4\cdot\cos\alpha+(k_3\cdot k_4)^2}$$

and

$$\text{Tan }\theta_b=\left\{\frac{k_3\cdot k_4\cdot\sin\alpha}{k_2+k_3\cdot k_4\cdot\cos\alpha}\right\}$$

When resistance damping is neglected, $b=0$; $k_4=1.0$; $\omega_o/\omega=1.0$ and $\phi=90$ degrees. Then

$$K_b=K_o=\sqrt{1-2k_2k_3(1-\cos\alpha)}$$

The undamped recovery voltage then becomes

$$e=E_1-(E_1-e_{B_1})\cdot K_o\cdot\sin(\omega_o t+90^\circ-\theta_o)$$

where

$$\text{Tan }\theta_o=\left\{\frac{k_3\cdot\sin\alpha}{k_2+k_3\cdot\cos\alpha}\right\}$$

When no diversion occurs, $k_3=0$ and $k_3=1$. Hence $\theta_o=\alpha=\psi=0$ and $K_o=1.0$.

Under these conditions,

$$e=\{E_1-(E_1-e_{B_1})\cdot\cos\omega_o t\}$$

Then should the arc voltage, $e_{B_1}=0$ and $E_1=E_m$, we obtain $e=E_m(1-\cos\omega_o t)$.

This is the simplest recovery voltage for a single-frequency circuit and the one normally assumed in determining the basic rate of rise of voltage of the circuit independent of the action of the interrupter. In contrast, the recovery-voltage expression of equation 43 includes all aspects of displacement, resistance damping, arc voltage, and the complete pendulum action of the current in the region of current zero.

For all the equations of the general case involving k_2 and k_3 it is clear that they are only valid for values of t equal to or greater than α/ω. For values of time less than α/ω the recovery voltage is expressed by the equation

$$e=e_{B_1}+k_2e''(t) \qquad (46)$$

which becomes

$$e=k_3e_{B_1}+k_2\left[E_1-(E_1-e_{B_1})\frac{\omega_o}{\omega}\epsilon^{-bt}\times\sin(\omega t+\phi)\right]\text{for }t<\alpha/\omega \qquad (47)$$

Nomenclature

E = generated voltage, open circuit across breaker
E_m = crest magnitude of generated voltage, assumed 1.0
E_1 = instantaneous value of generated voltage at interruption
e_B = arc voltage, function of time
e_{B_1} = instantaneous arc voltage at current diversion
e = recovery voltage following current diversion
e_m = crest value of recovery-voltage transient
ψ = angle behind normal zero at which diversion occurs, radians*
α = angle from current diversion to actual current zero, radians*
$k_3=\psi/\alpha$ = per-unit slope of second triangular cancellation current
$k_2=(1-k_3)=\left(\frac{\alpha-\psi}{\alpha}\right)$ = per-unit slope of first triangular cancellation current

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* Radians based upon the period of the undamped natural frequency T_o being 2π radians.

The Capacitor—an Aid to Electric-Power Service for Pipe Lines

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IN the over-all design of an electrified oil pipe line, power service constitutes a major problem, not only because of its importance in the economic structure, but also because it is a problem more varied in its component parts than almost any other phase of the project. Station designs are for the most part repetitive; power-service facilities, up to the point of delivery, vary widely from station to station. In the completion of three recent major pipe-line projects, power service has been facilitated in numerous instances by capacitors; in some cases, under conditions imposed by wartime conservation of power-service materials, capacitors have been the key to successful station operation.

Power Service to Pipe-Line Pumping Stations

Trunk oil pipe lines of today range in size up to 24 inches in diameter and up to 1,400 miles in length. Such are the dimensions of the line popularly known as the "Big Inch," now under completion to carry 300,000 barrels of oil per day from Texas to the Atlantic seaboard. In usual modern trunk-line practice the task of propelling the oil through the pipe is performed by centrifugal pumps in stations at intervals along the line, with one, two, or three pumps operating in series in each station. In the major pipe-line projects of the last several years these pumps are driven by electric motors of the squirrel-cage induction type, in ratings ranging up to 1,500 horsepower per unit.

The installed motor capacity on a large line totals as much as 131,000 horsepower in the case of "Big Inch," 126,000 horsepower of which is applied to main-line pumping. Data on motor capacity, units per station, average spacing of stations, and number of stations are given in Table I for three major lines.

From this table it can be appreciated readily that power service is a large factor in pipe-line design, and, with load factors well above 90 per cent the rule, power cost is an important item in operating expense. As an indication of just how important it is, the annual power bill for operation of the "Big Inch" line will be some $6,000,000.

The whole character of pipe-line operation and, in turn, of power supply stems from subdivision into stations separated geographically but joined physically. Reference may be made to a discussion presented elsewhere[1] of the hydraulic conditions determining station spacing and location. A brief summary of it follows.

For a given condition of pipe, fluid, and rate of flow, the friction pressure drop per mile is fixed. Interchangeability of pumping equipment requires that the station incremental pressures be uniform throughout the line. Under these conditions the station sites are determined quite rigidly by the profile of the line, without appreciable flexibility. In regard to power service, then, station locations cannot be chosen to facilitate line construction; rather, power service must be extended to station sites as determined by pipe-line hydraulics.

In planning power service, consideration must be given, of course, to both the maximum running load and to voltage conditions incident to motor starting.

Figure 1. Three 1,500-horsepower 1,780-rpm motors with switchgear for incoming 2,300-volt power and reduced-voltage starting control in 24-inch-line station of War Emergency Pipelines, Inc.

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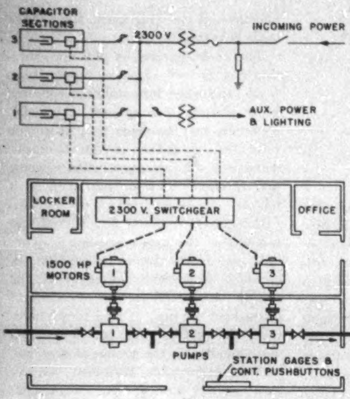

Figure 2 (left). Power-service and capacitor arrangement in 24-inch-line station of War Emergency Pipelines, Inc.

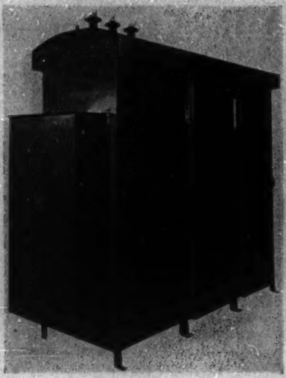

Figure 3 (right). Outdoor capacitor construction as used on certain 20-inch and 24-inch War Emergency Pipelines stations

Fortunately, from the standpoint of power supply, on the larger pipe lines employing the higher total horsepowers per station it is economical to divide the load into as many as three units. This results in a much more advantageous ratio of motor starting current to running current than exists in a single-unit station.

Capacitor Applications on the War Emergency Lines

The 24-inch line has, in general, three 1,500-horsepower motors per station, each with a full-load input of 1,285 kva at 0.92 power factor, or $1{,}181-j501$ kva. Starting current per motor is 1,500 amperes (locked) at 0.22 power factor, or $1{,}318-j5{,}840$ kva at 2,300 volts. Total running load per station is $3{,}546-j1{,}503=3{,}860$ kva. The demand with two units in operation and the third motor starting at full voltage is $3{,}682-j6{,}844=7{,}770$ kva, or about 200 per cent of station full-load kilovolt-amperes. A number of stations employ reduced-voltage starting by autotransformer, the demand during starting being then, with 80 per cent tap starting voltage, about $3{,}208-j4{,}740=5{,}730$ kva, or 150 per cent of station full-load kva. Figure 1 is a typical view of motors and switchgear in one of the 24-inch-line stations.

The 20-inch line has, in general, three 1,250-horsepower motors per station, each with a full-load input of 1,050 kva at 0.93 power factor, or $986-j394$ kva. Starting current per motor is 1,480 amperes (locked) at 0.24 power factor, or $1{,}370-j5{,}730$ kva at 2,300 volts. Total running load per station is $2{,}958-j1{,}182=3{,}180$ kva, and the demand with two units in operation and the third motor starting at full voltage is $3{,}342-j6{,}518=7{,}320$ kva, or 230 per cent of station full-load kva. With 80 per cent tap starting voltage, the demand during starting of the third unit is $2{,}849-j4{,}448=5{,}280$ kva, or 170 per cent of station full-load kilovolt-amperes.

Since the ratio of starting demand to full-load running demand is not excessive in these stations, the use of capacitors is directed primarily to improving the running-load power factor in order to facilitate carrying the station loads with minimum reinforcement of existing facilities.

On the 24-inch line, capacitors are employed at three station sites. In each case the total station bank is divided into three sections, as indicated in Figure 2. Each section is associated with one motor and is equipped with an electrically operated circuit breaker controlled to switch on and off simultaneously with the corresponding motor. Two of the stations have, each, three 720-kva sections, and one station, three 360-kva sections. Each 720-kva section operates in combination with its motor at full load with a slightly leading resultant power factor, making 219 capacitive kilovolt-amperes available to the power system. Each 360-kva capacitor section operates in combination with its motor with a slightly lagging resultant power factor, drawing 141 reactive kilovolt-amperes from the power system.

On the 20-inch line a similar arrangement of capacitors is employed at four stations; two stations having, each, three 720-kva sections, and two having, each, three 360-kva sections. Here each 720-kva capacitor and motor combination delivers 326 capacitive kilovolt-amperes to the power system, and each 360-kva capacitor and motor combination draws 34 net reactive kilovolt-amperes.

Each capacitor section is equipped with an overvoltage relay to trip the capacitor breaker in case of overvoltage resulting from self-excitation of the associated motor.

A 120-mile 110-kv transmission line along the pipe-line right of way will serve three station sites common to the 24-inch and 20-inch lines. In addition to capacitors at five stations at these three sites, two sections of 1,260 kva each are

Table I. Miscellaneous Data for Three Major Pipe Lines

Line	Plantation* (Refined Products)			War Emergency Refined Products	War Emergency Crude Oil
Pipe size (inches)	12	10	8	20	24
Barrels per day capacity	90,000	63,000	30,000	235,000	300,000
Approximate length (miles)	432	357	165	1,550	1,400
Total stations	14	14	4	29	28
Average station spacing (miles)	30.8	25.5	41.1	52.5	52.5
Pumps per station	2	2	1	3	3
Pump-motor horsepower per unit	900	600	600	1,250	1,500
Total motor horsepower installed on main pumps	26,100	17,400	2,400	108,350	126,000
Number of companies supplying power	10 for total line			16	17

* Data for main line only, from Baton Rouge, La., to Richmond, Va. Total mileage, including branch lines, 1,425; total number of stations, 35.

applied at the bus of the switching substation where this feeder originates.

These capacitors all consist of 15-kva one-phase 2,400-volt units, in outdoor housings. Oversize housings are provided for all sections at pumping stations, permitting later addition of capacitor units if desirable. Figure 3 shows one of these outdoor housings, built to accommodate a 1,080-kva capacitor section, initially installed with 720 kva of capacitor units. With interchangeability of capacitor units, the flexibility of this arrangement is apparent.

Capacitor Applications on Plantation Pipe Line

Somewhat more varied requirements governed the selection of capacitors on Plantation Pipe Line Company's refined-products line from Baton Rouge, La., to Greensboro, N. C. The original 14 main-line stations, built in 1941, were served without particular difficulty. However, when an increase in capacity was effected in 1942 by the addition of 14 booster stations, a number of the new station sites required considerable power-service extensions. Meanwhile, the development of a critical situation in power-line construction materials made their conservation imperative.

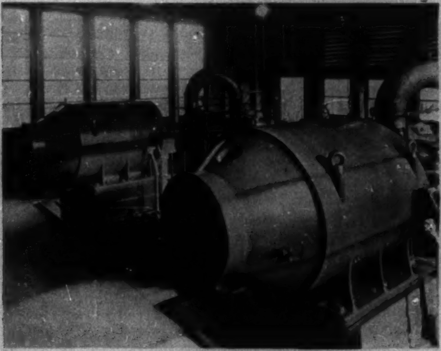

Figure 4. Two 900-horsepower 3,585-rpm motors driving 12-inch products-line pumps in intermediate station of Plantation Pipe Line

Each of the stations on the 12-inch section of the line contains two 900-horsepower motors. Starting requirements for these units have been discussed in another paper.[2] Each 900-horsepower motor has a full-load input of 776 kva at 0.91 power factor, or $706-j320$ kva. Starting current per motor is 1,200 amperes (locked) at 0.17 power factor, or $815-j4{,}700$ kva at 2,300 volts. The demand with one motor in operation at full load and one motor starting at full voltage is $1{,}521-j5{,}020$ or 338 per cent of station full-load kilovolt-amperes. Use of autotransformer starting on 85 per cent voltage tap reduces this demand to $1{,}296-j3{,}720$ kva which is 254 per cent of station full-load kilovolt-amperes.

In each of the seven new intermediate stations of the 12-inch line, it was found that shunt capacitors would conserve transmission-line capacity either by enhancing running power factor or by improving starting conditions to facilitate delivery of the required starting voltage. In co-operation with the power company, in each case, capacitors were installed in these stations as shown in Table II.

Views of the equipment in one of these stations are shown in Figures 4 to 7, inclusive.

In two of these stations where the power company installed transformers with load-tap changers, as shown in Figure 7, advantage was taken of this equipment as a further means of providing adequate motor-starting voltage by arranging the motor control automatically to run the tap changer to the full-boost position in preparation for each motor start, and then at completion of the start to return the tap-changing mechanism control to its own voltage relay for normal operation.

Intermittent Capacitor at Fluker Station

The capacitor application at Fluker station is of special interest. Under the power-rate schedule applying to this station there would be no return to the Pipe Line Company for operation at power factors higher than the normal running values attained by these units; consequently, the capacitor is employed for improvement of starting conditions only.

The station is served over a 23-kv circuit of number 2 copper, 17 miles long, having an impedance, including step-down transformer at the pumping station, of approximately $0.183+j0.387$ ohm per phase. With one pump unit operating at full load and at 2,750 rpm—critical speed from the standpoint of motor-torque margin—and with the second unit starting on 85 per cent autotransformer tap, the current demand is $320-j780$ amperes at 2,300 volts. This combination of line and load impedance indicates a voltage at the station bus of about 78 per cent of the sending voltage, a value inadequate to provide reliable starting.

It was decided to apply sufficient capacitance to reduce the current during starting to a value of $320-j315$ amperes at 2,300 volts. The resulting station-bus voltage was calculated as about 88 per cent of the sending voltage.

The corrective current of this capacitor, 465 amperes at 2,300 volts, corresponds to

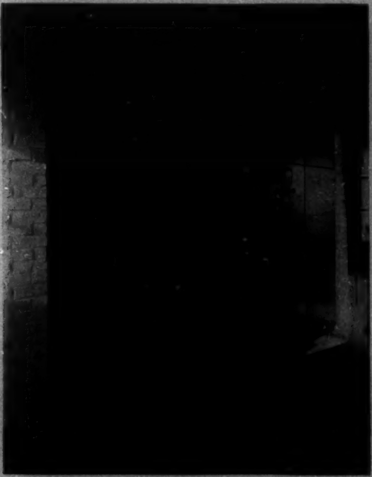

Figure 5. Switchgear for control of 2,300-volt incoming-power breaker, two 900-horsepower reduced-voltage motor starters, and auxiliary low-voltage power and lighting on Plantation Pipe Line. Control panel for single-section capacitor is at extreme left

Figure 6. Rear view of equipment shown in Figure 5, showing capacitor in foreground

a rating of 1,860 kva. To conserve materials and capacitor cost, an intermittent capacitor was designed for starting duty only. This was accomplished by employing units continuously rated at 1,750 volts and operated at 120 to 137 per cent of rated voltage during starting. This capacitor, rated 1,080 kva at 1,750 volts, provides the same corrective effect as would a standard 2,400-volt 2,030-kva capacitor and thus represents a saving of 950 kva or 47 per cent in applied capacitive kilovolt-amperes.

The capacitor was installed as a single section switched by an electrically operated circuit breaker. Its control is combined with the control of the two 900-horsepower pumping motors so that, when either motor is started, the capacitor is connected simultaneously to the station bus until the completion of the starting period, when it is tripped automatically by an auxiliary relay operated by the motor starter.

Comparison of the no-load saturation curve of the motor with the capacitor volt-ampere line indicated a possible self-excitation voltage of about 4,000 volts in case of service interruption with the capacitor and one motor at full speed on the bus, and about 3,500 volts with the capacitor and two motors, one running and the other just completing a start.[3] An overvoltage relay, therefore, was included in the capacitor control to trip the capacitor breaker, limiting the overvoltage duration to a few cycles.

TESTS AT FLUKER STATION

Oscillograph tests were made at this station to check the starting performance of the installation. Table III gives comparative results with and without the starting capacitor: pump 1 was operating at full speed and about 76 per cent load, and pump 2 was started with closed discharge valve. These tests were made with the pumps operating on kerosene of 0.80 specific gravity, a condition imposing about eight per cent lower pump-load torque during acceleration than the maximum design condition handling 0.867 specific-gravity fuel oil.

Without the aid of the capacitor, the voltage during starting dropped to a value below that permissible with the line handling fuel oil, although the unit did start successfully handling kerosene.

This test was made with the motor starting on 80 per cent autotransformer taps, a condition imposing about 89 per cent of the demand that would be developed using 85 per cent taps. This condition was the basis of design of the capacitor installation. Calculation of voltage conditions with motor 1 running and motor 2 starting on 80 per cent of the autotransformer taps indicated a bus voltage of 79.8 per cent of sending voltage with the capacitor in service. The test results are in fair agreement with these values. Sections of the oscillograms for these tests are shown in Figure 8.

Tests also were made to measure the self-excitation voltage obtained with this combination of motors and capacitor. In each case the incoming line breaker was tripped with the motor (or motors) and the capacitor connected to the bus, the motor being at full speed and the pump discharge valve closed—a condition developing about half load on the motor.

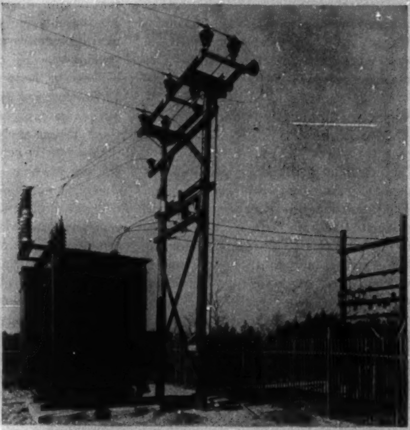

Figure 7. This substation with completely self-protected transformer, which serves the pumping station on Plantation Pipe Line, features minimum use of construction materials

The self-excitation voltage, measured with the capacitor and one motor, was 3,760 volts at 98 per cent motor speed. The predicted voltage was about 4,000 at full speed, or about 3,920 volts at 98 per cent speed. The self-excitation voltage, measured with the capacitor and two motors on the bus, was 3,200 volts at 96.5 per cent speed; the calculated voltage corrected for motor speed was about 3,380 volts.

Motor Starting at Roxboro Station

MOTOR AND PUMP CHARACTERISTICS

The Roxboro, N. C., pumping station, on the eight-inch extension of the Plantation Line from Greensboro, N. C., to Richmond, Va., contains one main-line centrifugal pump. This pump is coupled to a 2,300-volt three-phase 60-cycle squirrell-cage motor with a 600-horsepower 3,586-rpm rating. Full-load input is 131.2 amperes at 0.91 power factor or $417-j219$ kva. The motor is started on full voltage. Locked rotor current at 2,300 volts is 823 amperes at 0.17 power factor, or $558-j3{,}240$ kva. This is 626 per cent of full-load kilovolt-amperes.

The motor has speed-torque and speed-current characteristics by test as shown in Figure 9, curves 1 and 3. Curve 5a hows the speed versus torque of the pump when it is started with the discharge valve closed, while handling fuel oil of 0.867 specific gravity, the heaviest product anticipated. Comparing the motor speed–torque curve with the pump speed–torque curve, it is evident that, under reduced-voltage conditions, the motor torque will have minimum excess above the required pump load torque in the region of 2,800 rpm. For reliable acceleration it is necessary to deliver not less than 1,760 volts to the motor in the critical speed range, permitting the motor to develop a minimum margin of ten per cent over the pump torque. The maximum sustained voltage allowable at the motor under continuous operation is 2,530 volts.

The motor current at 2,800 rpm with 1,760 volts applied is by shop test 603 amperes per phase, at approximately 0.20 power factor (curve 4). In the calculations forming the basis of application, a slightly higher motor current was used, obtained from tests on a motor of similar characteristics. This current, 658 amperes at 0.20 power factor for 1,760 volts and 2,800 rpm, is within the range of manufacturing variation that may be encountered on individual motors of the same design.

Conditions of Power Service

In serving this pumping station it was necessary in the interest of line-materials conservation to make use of an existing 12-kv power line of number 2 copper from the power company's 7,500-kva 132/12.4-kv substation to a point about 16.4 miles distant, where a new tap line of number 2 copper extends about three miles to the pumping-station site.

The total impedance of the supply to the 2,400-volt bus at the pumping station is approximately 0.745 + j 0.875 ohm per phase, referred to the 2,400-volt bus.

Starting Voltage Without Corrective Means

From the motor impedance at 1,760 volts and the total impedance of supply, the calculated starting voltage at the motor, assuming a sending voltage of 2,530 and no other load on the feeder, is 1,388 volts. This is below the minimum allowable voltage for starting the motor. Alternative service facilities were considered, but were not permissible from the standpoint of critical-materials conservation.

Capacitor Requirement

The power company agreed to serve the pumping station over this circuit and to deliver a minimum voltage of 1,760 during starting, provided Plantation Company install a two-section capacitor at the pumping station bus.[4] With both sections energized during starting of the pump motor, the total bank was required to provide a capacitive effect of 1,185 kva at 1,760 volts. In arriving at this value of capacitance it was assumed that, simultaneously with the motor-starting demand, there would also exist on the feeder 80 per cent of the distribution load, or 355 kw at 0.80 power factor.

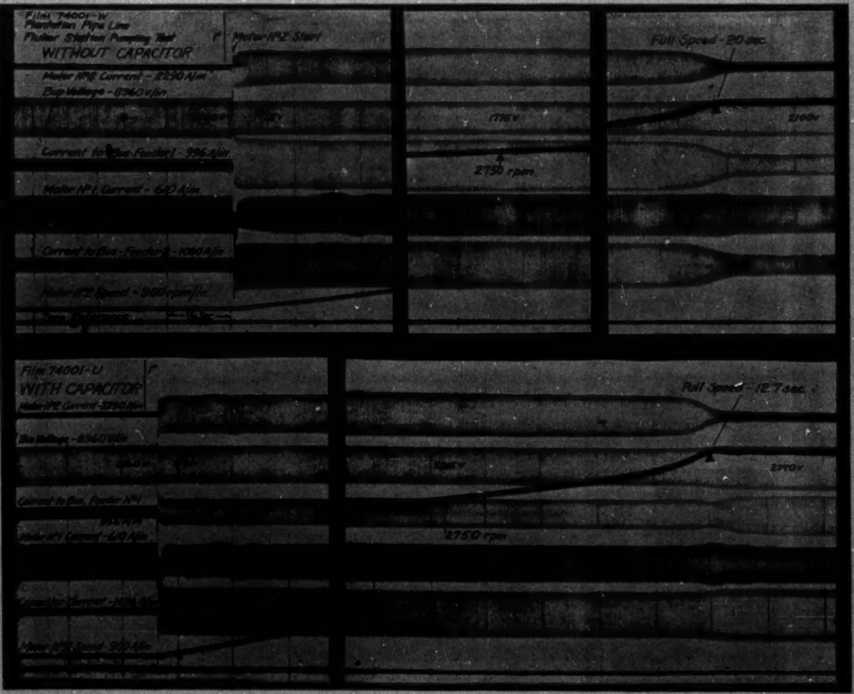

Figure 8. Sections of oscillograms showing starting performance of 900-horsepower pump motor from the Fluker station test

After completion of the motor-starting period, one section is switched off, leaving an effective capacitive kilovolt-ampere of 650 at 2,530 volts, which is the calculated voltage attained with this capacitor, with the motor running at full rated load, and with a 20 per cent distribution load. The capacitive kilovolt-amperes of 650 at 2,530 volts was developed by applying a standard 2,400-volt capacitor of 585-kva rating ($-j9.85$ ohms per phase, line to neutral) as the continuous-running capacitor section.

Since the required total effective capacitive kilovolt-amperes at 1,760 volts must be 1,185 kva, 315 of which are obtained at this voltage from the continuous-running capacitor, the difference or 870 kva at 1,760 volts must be provided by the second capacitor section. This section, being in service only during starting of the motor, was designed for intermittent operation.

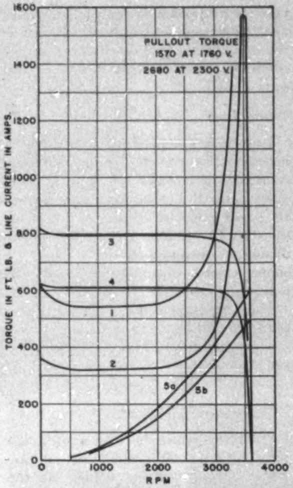

Figure 9. Starting characteristics of 600-horsepower pump drive on eight-inch products line

1. Speed–torque for motor at 2,300 volts
2. Speed–torque for motor at 1,760 volts
3. Speed–current for motor at 2,300 volts
4. Speed–current for motor, at 1,760 volts
5. Speed–torque for pump starting with closed discharge

(*a*). With fuel oil of 0.867 specific gravity
(*b*). With gasoline of 0.73 specific gravity

Feeder Voltage Regulation as Affected by Capacitor

Investigation indicated that application of a capacitor as large as 1,185 kva at 1,760 volts for starting conditions would introduce a critical problem of voltage regulation. At the end of the motor-starting cycle, when the motor approaches full speed, its starting current suddenly decreases to a low value, leaving a large net capacitor current on the bus until the starting capacitor can be tripped off. This results in an increase in voltage at the bus, which in turn increases the capacitor current, and this further increases the voltage; the condition stabilizes at a value calculated as about 3,200 volts, a value undesirably high.

Consideration was given to the possibility of reducing the peak voltage by relaying, responsive to change in motor power factor or impedance, to trip the starting-capacitor breaker as soon as the motor reaches a speed at which this capacitor is no longer necessary. However, the capacitor is required until the unit is well up to full speed, after which acceleration is extremely rapid because of the low inertia of the motor and pump and the suddenness with which the motor torque increases after passing the critical range. While such relaying would serve to limit the duration of overvoltage to within a fraction of a second, it should be borne in mind that this overvoltage would occur each time the motor is started. This condition was undesirable from the standpoint of service to the pipe line and to other customers on the line.

Method of Limiting Line-Regulation Voltage

The problem thus devolved into finding a means of preventing the occurrence of this overvoltage. The attack was to devise a way of neutralizing the increase in capacitor current inherent with increase in applied voltage.

The solution was effected by providing, in opposition to the capacitor, an inductive reactance suitably controlled throughout the starting cycle. Such control implies a small reactive current at the low voltage existing during most of the starting period, but a large reactive current when the voltage increases near the end of the starting period, to balance the excess capacitor current at the higher voltage. One method proposed for accomplishing this control was use of an iron-core reactor designed to become saturated at a voltage slightly above that at which the capacitor effect is primarily desired.

In the case of Roxboro station, an economical intermittent capacitor bank was devised by using standard 2,400-volt capacitor units and connecting the bank to the station bus through an autotransformer so as to impress upon the capacitor bank a voltage about 1.4 times the bus voltage. Thus the operating kilovolt-amperes of the capacitor is increased to about twice the kilovolt-amperes at which the capacitor would operate if connected directly to the bus.

It is apparent that the autotransformer supplying the intermittent capacitor section can be utilized to provide the necessary controlled reactive current for limiting the effect of the capacitor in the upper range of voltage. To accomplish this, the magnetizing current of the autotransformer at 1,760 volts should be as low as feasible, while, at voltages above 2,400, saturation should occur with resultant sharp increase in lagging current.

Laboratory Investigation

To determine quantitatively the required autotransformer proportions, certain tests were conducted:

1. An extended no-load saturation test was taken on a 600-horsepower motor identical with the one at Roxboro pumping station. Whereas ordinary test procedure is to carry the no-load saturation test up to a voltage of about 20 per cent above rating, in this test readings were taken with voltages up to 200 per cent normal. Results are plotted in curve *M* of Figure 13.

2. A series of tests was made on a capacitor bank electrically similar to the proposed

Table II. Capacitor Applications in Plantation Pipe Line 12-Inch-Line Intermediate Stations

Station	Installed Capacitive Kilovolt-Amperes	Number Capacitor Sections	Operation	Method of Control
Fluker	1,080	1	Intermittent	Energized only during starting of either motor
Hathorn	810	2	Continuous	Both sections energized during operation of either motor
Paulding	225	1	Continuous	Energized during operation of either motor
Boyd	1,260	2	Continuous	Each section energized during operation of associated motor
Coaling	630	1	Continuous	Energized during operation of either motor
Vincent	630	1	Continuous	Same as Coaling
Heflin	630	1	Continuous	Same as Coaling

All sections consist of 15-kva one-phase 2,400-volt units, except Fluker, which consists of 15-kva one-phase 1,750-volt units.

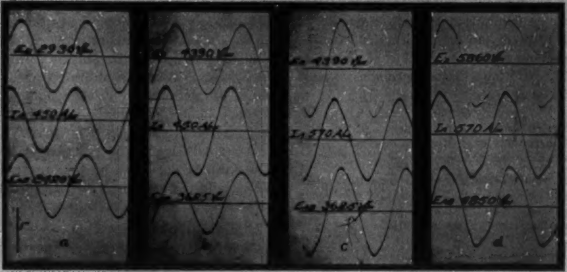

Figure 10. Oscillograms from combination test of autotransformer and capacitor

E_{AB} = voltage, phase *AB*, applied to autotransformer (65 per cent tap)

E_2 = voltage, phase *AB*, on capacitor (100 per cent tap)

I_A = line current, phase *A*, on input side of autotransformer

Test	E_{AB}	E_2	I_A
a	1,198	1,688	230 = 0 + *j*230
b	1,770	2,440	314 = 34.7 + *j*313
c	2,560	3,445	377 = 97.5 + *j*364
d	3,120	4,140	391 = 188 + *j*364

intermittent section at Roxboro pumping station, in combination with an autotransformer.

The test capacitor was a three-phase delta-connected, bank, consisting of 19 capacitor units in parallel per phase, each unit being rated 15 kva, 2,300 volts. The autotransformer was of the standard motor-starting type, rated 600 horsepower, 2,300 volts, three phase, open delta, with voltage taps of 50, 65, 80, and 100 per cent. This unit was connected to deliver current from its 100 per cent taps to the capacitor, and to receive current on its 65 per cent taps from a large testing plant generator. To simulate line conditions at Roxboro station, a series impedance, equivalent to $0.80+j0.90$ ohm per phase was inserted on the low side.

A series of oscillograms was made recording autotransformer input voltage and current, and autotransformer output voltage, over a range of input-voltage values up to about 3,100 volts. Figure 10 shows typical oscillograms.

From these tests the following data were derived:

1. The volt-ampere characteristic curve for the combination of capacitor and autotransformer was plotted as shown in curve *A*, Figure 11.

2. From each test oscillogram the input power factor was calculated, and the total input current was separated into its power and wattless components. The power-component values are shown in curve *B*, and wattless-component values in curve *C*.

3. The net volt-ampere curve for the test capacitor referred to the input side of the autotransformer is shown in curve *D*. This is a straight line, obtained by actual measurement of the capacitance of the capacitor ($-j5.88$ ohms, line to neutral), then deducting the series reactance of the autotransformer to obtain a net resultant capacitance for the combination. The value obtained, referred to the low side of the autotransformer is $-j2.83$ ohms per phase, line to neutral. For a line-to-line voltage of 2,400 on the low side, the corresponding current is 490 amperes. Plotting these two values determines the effective volt-ampere curve for the test capacitor.

4. The reactive current (curve *C*) is the resultant of the leading reactive current of the capacitor (curve *D*) and the lagging magnetizing current of the autotransformer; hence, the latter is obtained by subtracting the current values on curve *C* from those on curve *D*, giving curve *E*.

It is apparent that, with increasing voltages, as saturation occurs in the autotransformer, the magnetizing current drawn by the autotransformer assumes disproportionately greater values, neutralizing to an increasing extent the capacitor current and, finally, at about 2,700 volts, causing a decrease in the total reactive current.

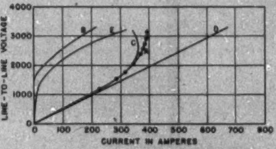

Figure 11. Test volt-ampere characteristic curves for capacitor–autotransformer combination. Capacitive reactance = $-j2.83$ ohms

- A. Total current input to combination
- B. Power-component current
- C. Wattless-component current
- D. Capacitor current (input side)
- E. Autotransformer magnetizing current (D–C)

This effect, of course, can be increased by increasing the magnetizing current drawn by the autotransformer. In Figure 12, curve *F* is a magnetization characteristic calculated for two autotransformers, identical with the one tested, connected in parallel to feed an intermittent capacitor.

In this case, the capacitor consists of 78 standard 15-kva single-phase 2,400-volt units, with 26 units per phase.

The capacitive reactance of the capacitor bank alone is 4.92 ohms, line to neutral. The series reactance of the autotransformer is now half the value previously used. The resultant effective capacitive reactance referred to the low side is 2.44 ohms, line to neutral. For a line-to-line voltage of 1,760 on the low side, the phase current is 416 amperes. These two values determine curve *G*.

At 1,760 volts the magnetizing current drawn by the autotransformer bank is 98 amperes, leaving a net effective capacitor current of 318 amperes, or 970 bank kilovolt-amperes at 1,860 volts. This approximates the 312 effective amperes for the test capacitor. Either capacitor bank provides about ten per cent margin of effective capacitive current at 1,760 volts above the minimum requirement for the intermittent capacitor at Roxboro station.

Curve *H* is the total reactive current for the combination, and is obtained by subtracting the current values of curve *F* (transformer magnetizing current) from those of curve *G* (capacitive current). The power-component-current curve *K* is constructed by doubling the power-component currents obtained in curve *D*, Figure 11, for a single autotransformer. Finally, the total current curve *L* is obtained by combining the power- and total reactive-current curves *K* and *H*.

Next, consideration was given to the self-excitation voltage possible with the

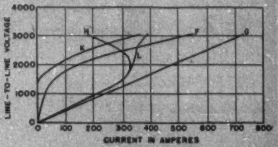

Figure 12. Calculated volt-ampere curves for combination of standard 2,400-volt capacitor and double autotransformer. Capacitive reactance = $-j2.44$ ohms

- F. Autotransformer magnetizing current (2*E*)
- G. Capacitor current (input side)
- H. Wattless-component current (*G–F*)
- K. Power-component current (2*B*)
- L. Total current input to combination (*K*+*jH*)

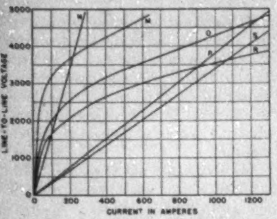

Figure 13. Self-excitation characteristics for 600-horsepower motor and capacitors

M. Excitation current for one motor

N. Capacitor current for continuous capacitor ($X = -j9.85$ ohms)

O. Excitation current for one motor plus single autotransformers ($M+E$)

P. Capacitor current for test capacitor plus running capacitor ($D+N$)

R. Excitation current for one motor plus double autotransformer ($M+2E$)

S. Capacitor current for intermittent capacitor plus running capacitor ($G+N$)

capacitor banks and the motor connected to the pumping-station bus, the motor at full speed, and the bus isolated from the power supply by tripping of the main breaker. Two operating conditions must be considered:

1. In normal running operation, the 585-kva running capacitor will be connected to the bus with the motor.

2. During starting, both capacitor banks will be in use, and self-excitation may occur with this combination at full motor speed at the end of the motor-starting cycle.

Consider first the conditions obtaining during running. The maximum value of self-excitation voltage with no load on the station bus may be predicted by finding the value at which the no-load excitation curve for the motor intersects the volt-ampere characteristic of the continuous capacitor. As shown in Figure 13, curves *M* and *N*, this occurs at about 3,750 volts.

Now, considering the starting condition, the intersection of the combined magnetization curve for the motor and test autotransformer (curve *O*) with the volt-ampere characteristic of the effective test capacitor, plus the running capacitor (curve *P*), occurs at about 4,750 volts. The combined magnetization curve for the motor and two autotransformers (curve *R*) intersects the volt-ampere line of the combined intermittent capacitor plus the continuous capacitor (curve *S*) at about 3,600 volts. In practice these values will be decreased because of the loading effect of the losses in the autotransformer feeding the intermittent bank.

In making the capacitor application at Roxboro station, circumstances dictated use of equipment promptly available; hence, it was decided to employ for the intermittent capacitor bank the 2.44-ohm capacitor combined with two autotransformers identical with the autotransformer used in the laboratory test.

Use of two autotransformers in preference to one was for reasons of:

1. Sharper limitation of net capacitive current at voltages above 1,760, and, therefore, less overvoltage at the end of the starting cycle.

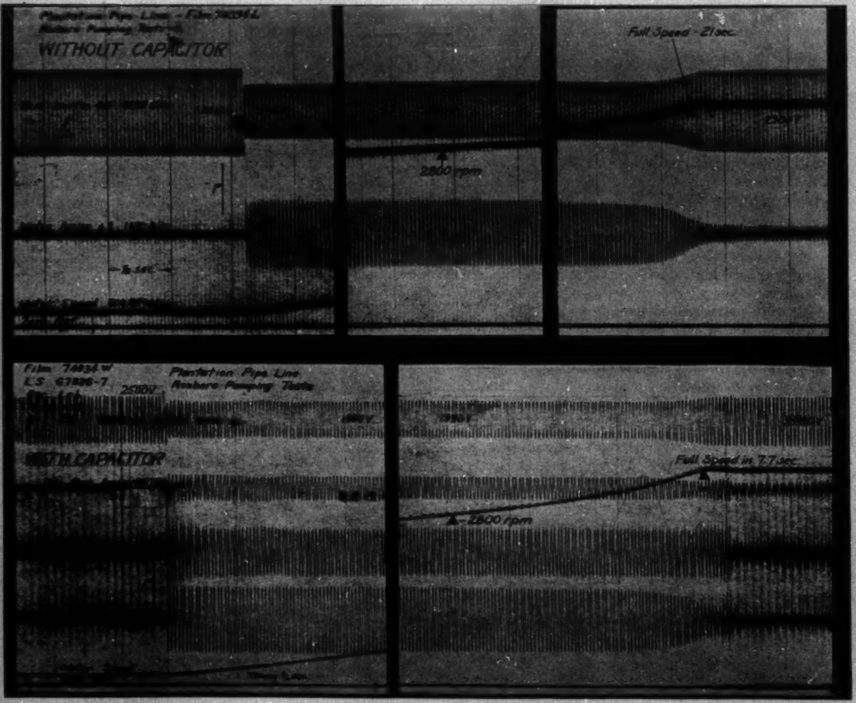

Figure 14 (below). Sections of oscillograms showing starting performance of 600-horsepower motor, from the Roxboro station test

2. **Lower self-excitation voltage in case of power interruption during the starting cycle.**

For this capacitor–autotransformer combination, the maximum voltage on the pipe-line station bus at the end of the starting cycle was calculated as 2,605 volts, a value quite acceptable from the operating standpoint.

Oscillograph Tests at Roxboro Station

After installation of this equipment, oscillograph tests were made to check the performance of the apparatus under service conditions. During these tests the pump was operating with gasoline, and its load torque during acceleration thereby was reduced from the values shown in Figure 9, curve 5*a*, in the approximate ratio of the specific gravities of gasoline and fuel oil which equals 0.842. This results in a somewhat lower present speed–torque curve, 5*b*, meaning that, when gasoline is being handled, a lower starting voltage is permissible on the motor. Comparison of curves 1 and 5*b* in Figure 9 indicates that for zero torque margin the voltage at the motor at 2,800 rpm would be about 1,520 volts.

A test was made starting the unit without any capacitors. The motor accelerated the pump with a very narrow margin of torque, requiring 21 seconds to reach full speed. The oscillogram showed a voltage of 1,530 at 2,800 rpm. Since the observed margin of torque is extremely small, starting performance under this condition would be unreliable. Furthermore, during the major portion of the start, the voltage dropped to a sustained value of 1,440, or 60 per cent of the no-load voltage, a behavior unacceptable from the standpoint of service to other customers on the line. For these reasons, regular operation without the aid of starting capacitors is impractical even when the line is handling gasoline, and would be impossible with fuel oil.

Table III. Results From Tests at Fluker Station on the Starting Performance of Capacitor

	Starting Capacitor Inoperative	Starting Capacitor Operative
Accelerating time for pump 2 (seconds)	20.5	12.8
Bus voltage:		
Before starting pump 2	2,190	2,160
With pump 2 at 2,750 rpm	1,775	2,015
Both pumps at full speed (capacitor energized)		2,370
Both pumps at full speed (capacitor disconnected)	2,100	2,070

Tests with the complete capacitor installation in use showed initial starting voltage sustained values of 1,870 to 1,960, or 76 to 79.2 per cent of the observed bus voltages with no station load. At 2,800 rpm motor speed, voltages ranged from 1,900 to 2,020. Voltages with the motor at full speed and with capacitors on the bus ranged from 2,515 to 2,700 exceeding by 4 to 6 per cent the observed station-bus voltages with no station load.

The majority of these tests measured voltages across one phase. To check voltage balance, a test was made measuring the three-phase voltages, and the results indicated bus-voltage unbalance expressed in terms of maximum deviation from average as percentage of average: at no load, three per cent; with motor accelerating, four per cent; and with motor at full speed and capacitor connected to bus, 11 per cent.

It is apparent that conditions at this installation are not ideal from the standpoint of voltage balance, firstly, because the transformer bank serving the station is composed of dissimilar units, and secondly, because the autotransformer supplying the intermittent capacitor is of open-delta construction. These conditions were necessities of the times. The unbalance inherent in the use of an open-delta autotransformer can be obviated readily by employing an autotransformer of three-phase three-leg construction. By special design this autotransformer also can be given more desirable saturation characteristics.

Additional oscillograph tests were made to observe the self-excitation voltage effects obtainable.

A test made with the continuous capacitor only showed a maximum self-excitation voltage on the station bus of 3,360 volts, with a motor speed of 94 per cent. The corresponding self-excitation voltage predicted from curves *M* and *N*, Figure 13, corrected to a motor speed of 94 per cent, is 3,370 volts.

A test made with both continuous and intermittent capacitors on the bus showed a maximum self-excitation voltage of 2,910, at a motor speed of 98 per cent. The value of 3,600 volts indicated from curves *S* and *R*, Figure 13, corrected to 98 per cent motor speed, is 3,520 volts.

This combination of capacitor and saturating autotransformer has been in satisfactory operation at Roxboro station since June 1943. The scheme of control should extend the field of usefulness of the capacitor by permitting a relatively large capacitor to be used with a squirrel-cage induction motor for purposes of improving starting conditions without incurring the overvoltage effects normally experienced in such applications. Just as Roxboro station is being served over an otherwise impossible circuit, this solution should point the way to transmission-line conservation in many situations involving single-unit loads in isolated locations.

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Electric Equipment for the War Emergency Pipe-Line Projects

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Synopsis: In the field of transportation of petroleum and its products by pipe line, the 24-inch and 20-inch lines, built by War Emergency Pipelines, Inc., from Texas to the Middle Atlantic Coast as emergency war projects, rank as world record breakers. The 24-inch line for crude oil is the largest ever built, and pipe-liners have picturesquely named it the Big Inch. The 20-inch products line is the largest for refined products and the longest line in existence, and is called the Little Big Inch. All pumping stations on both of these lines are operated by electric power, and in its application a number of interesting engineering problems developed. Results of the consideration given to these problems to meet successfully the anticipated operating conditions are given in this paper, and may be grouped as covering

1. Adequate protection of main pump motors and their circuits when supplied with power through transformers having various connections.
2. Use of capacitors for maintaining voltage on "soft" power systems, and their proper switching for optimum effect.
3. Power-utilization-equipment performance obtained as the result of emergency engineering under war conditions.

PRIOR to World War II, nearly all the petroleum and products requirements of the eastern states were supplied by coastwise tankers running from the Gulf Coast of Texas to the refining areas on the coasts of New York, New Jersey, and Pennsylvania. Very little came by pipe line and still less by tank car, as tankers provided transportation at much lower cost. With the advent of war, many of these tankers were withdrawn for overseas service, and it was necessary eventually to withdraw the remainder because of the submarine attacks along the Atlantic Coast and in the Gulf of Mexico.

Fortunately thousands of tank cars, most of them on railroad sidings where they had been idle for years, were available immediately and were pressed rapidly into service by the railroads to bring the oil up from Texas to the East, but at best they were able to transport a maximum of only about 900,000 barrels per day to supply the normal eastern requirement of 1,400,000 barrels per day plus the rapidly growing needs of the Armed Forces overseas. It was imperative that all of these needs be met; consequently, a general revamping of eastern pipe-line systems was inaugurated, and construction of the 24-inch and 20-inch lines was planned and authorized with this object in view.

General Description

The 24-inch line (Figure 1) extends from Longview, Tex., through Norris City, Ill., to Phoenixville, Pa., a distance of 1,254 miles, where it divides into two 20-inch branches, one to Marcus Hook, Pa., a distance of 23 miles, and the other to Linden, N. J., a distance of 86 miles. Four feeders in Texas bring the oil to Longview, and numerous distribution lines extending from the eastern terminals deliver it to the various refineries. The line has a rated capacity of 300,000 barrels (12,600,000 gallons) of crude oil per day, and will handle as high as 325,000 barrels per day in the summer months when the viscosity is lower. At rated capacity, oil flows through the line at a velocity of 6.1 feet per second, or 100 miles per day.

The 20-inch products line (Figure 1) begins at Beaumont, Tex., and runs through Louisiana to Little Rock, Ark., where it meets and parallels the 24-inch line all the rest of the way east over the same right of way. The branches east of Phoenixville are 16 inch, and a 16-inch feeder from Baytown, near Houston, Tex., to Beaumont, Tex., is part of the project. This line has 1,485 miles of 20-inch pipe and 157 miles of 16-inch pipe. The rated capacity for gasoline is 235,000 barrels (9,870,000 gallons) per day. The capacity is about ten per cent less for kerosene and 20 per cent less for heating oil or other distillates. The velocity of gasoline flow through the line is 7.5 feet per second, or 123 miles per day.

PUMPING STATIONS ON 24-INCH PIPE LINE

The 24-inch line has 29 pumping stations, excluding the eastern terminals. Their average spacing is approximately 53 miles, but it varies from a minimum of 31 miles on the upgrade over the Allegheny mountains in Pennsylvania to a maximum of 78 miles on the downgrade farther east. Two of the stations on the New Jersey branch are not now fully equipped, and will be used only when it may be desired to deliver the full capacity of the main line through that branch. There are now a total of 82 main pumps, each 1,500 horsepower, and 17 auxiliary booster pumps, of which three are 500 horsepower, three are 250 horsepower, and 11 are 100 horsepower.

The 100-horsepower booster pumps are used to maintain adequate suction head on the main pumps. They are located at pump stations near tank farms and upgrades; namely, three at Station 1, three at Station 11, and one each at Stations 16, 21, 22, 23, and 24. Many other pumps are used, some for supplying oil to the pipe line and others for delivering it to tank cars, other pipe lines, and terminals. These include two 350-horsepower delivery pumps and two 250-horsepower loading-rack pumps at Station 11, two 400-horsepower delivery pumps at Linden, N. J., and also, one 20-horsepower sump pump and three one-horsepower exhaust fans at every station. The present total connected load is approximately 128,500 horsepower.

At one station location in Missouri there was insufficient electric power available to operate the station as a single unit and it was necessary to spread the load by inserting another station 18 miles farther east in Illinois and dividing the main pumps between them. Another station, in Pennsylvania, has an extra main pump installed for use in case of a shutdown of any of the three normally used pump units, because this station is located at the beginning of a steep grade to the East and two pumps alone could not provide sufficient pressure to push any oil over the top.

TYPICAL PUMPING STATION, 24-INCH LINE

With these exceptions, the typical pumping station (Figure 2) has three main 16-inch by 12-inch single-stage centrifugal pumps (Figure 3) rated 8,750 gallons per minute at 630 feet head. These pumps are connected in series and are bypassed by check valves so that any pump can be shut down without stopping the flow of oil through the line. Each pump

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produces approximately 240 pounds pressure. Suction head on the upstream pump is approximately 30 to 40 pounds and station discharge pressure approximately 750 pounds.

Each pump is driven by a 1,500-horsepower 1,800-rpm 2,300-volt squirrel-cage induction motor of the general type shown in Figure 4, and direct-connected to it through a fire wall. The motors are of open-frame construction with ventilating air discharged at the top where it is picked up through ducts by exhaust fans and delivered to the pump room or outdoors in accordance with room-temperature requirements. The motors are limited by their design to 500 per cent starting current at full voltage. Fourteen of the 29 stations are equipped with reduced-voltage starters and the others with full-voltage starters, as specified by the power companies. Full compliance with the recommendations of the power companies in using reduced-voltage starting equipment at some of these stations is an example of the emergency engineering measures that were necessary to complete these projects on the fast schedule planned, as insufficient time was available to make a study of power conditions in advance of purchases of electrical equipment.

On installations such as these, where a large transformer serves one group of a few motors, there is considerable reactance in the transformer and the high-voltage feeder, which acts the same as a line-starting reactor in reducing the motor voltage under starting conditions. Whether reduced-voltage starting equipment can be of much further value in this respect depends upon the stiffness of the power system and the character of the feeder supplying the power for this load, and upon the margin in motor-accelerating torque above that required by the pump. Because of the fact that the high-voltage feeders built to reach the pumping stations carry no other load, and that the load itself is closely unified, the reduced-voltage starting method has less value to the power company than in areas of concentrated but unrelated loads that include lighting. Under the most unfavorable power conditions at these pumping stations equipped with reduced-voltage starting equipment, the relaying of the running breaker can be adjusted to put the motor on the line while it is accelerating, before the margin of torque available for acceleration is too greatly reduced. Under these circumstances a reduction of starting kilovolt-amperes of approximately ten per cent below that required for full-voltage starting is all the advantage that reduced-voltage starting will give.

Metal-clad switchgear of the general type shown in Figure 5 is used in all pumping stations for control of the main pump motors. Each installation consists of an incoming-line unit, an auxiliary unit, and starting units for each motor, all equipped with electrically operated (automatic-trip) oil circuit breakers having an interrupting rating of 100,000 kilovolt-amperes. The switchgear is located in the same room with the motors, separated from the pumps by the fire wall.

A typical one-line station-power diagram is shown in Figure 6.

In addition to the usual protection against undervoltage, motor and station overcurrent, and short circuits, suitable pressure switches are provided on each pump to function at abnormally low or high pressures. If the station suction head drops to 35 pounds, an alarm is sounded automatically. If it goes as low as 15 pounds, the three main motors

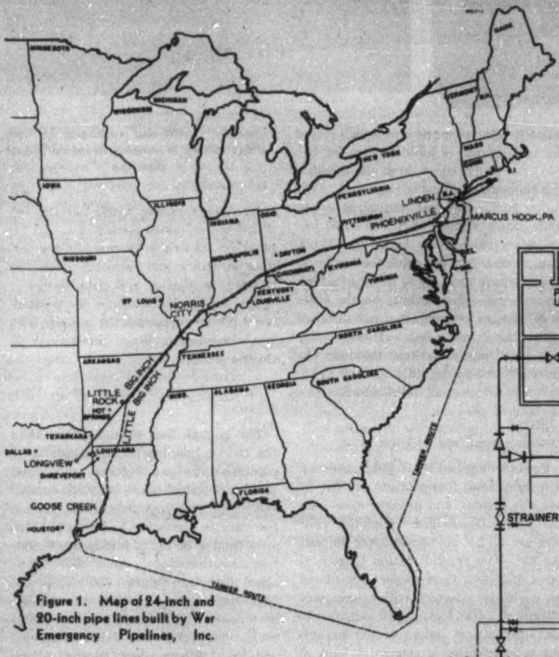
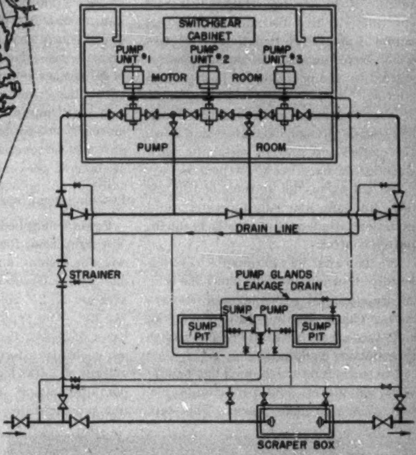

MAINE
VERMONT
N.H.
MASS.
CONN.
R.I.
NEW YORK
MINNESOTA
WISCONSIN
MICHIGAN
IOWA
ILLINOIS
INDIANA
OHIO
PENNSYLVANIA
LINDEN
N.J.
PHOENIXVILLE
MARCUS HOOK, PA
PITTSBURGH
DEL.
INDIANAPOLIS
DAYTON
CINCINNATI
W.VIRGINIA
VIRGINIA
MISSOURI
ST LOUIS
NORRIS CITY
KENTUCKY
LOUISVILLE
NORTH CAROLINA
TENNESSEE
ARKANSAS
BIG INCH
LITTLE BIG INCH
LITTLE ROCK
HOT SPRINGS
SOUTH CAROLINA
MISS.
ALABAMA
GEORGIA
TEXARKANA
DALLAS
LONGVIEW
SHREVEPORT
LOUISIANA
FLORIDA
GOOSE CREEK
HOUSTON
TANKER ROUTE
TANKER ROUTE

Figure 1. Map of 24-inch and 20-inch pipe lines built by War Emergency Pipelines, Inc.

Figure 2. Layout of typical three-unit pumping station on 24-inch oil pipe line


SWITCHGEAR CABINET
PUMP UNIT #1
PUMP UNIT #2
PUMP UNIT #3
MOTOR
ROOM
PUMP
ROOM
DRAIN LINE
STRAINER
PUMP GLANDS LEAKAGE DRAIN
SUMP PUMP
SUMP PIT
SUMP PIT
SCRAPER BOX

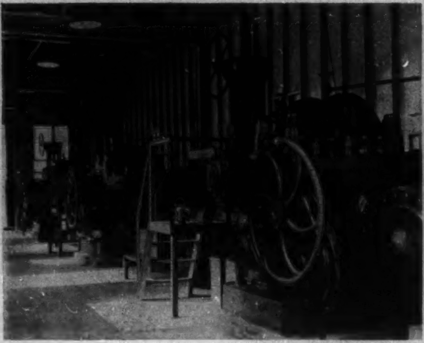


Figure 3. Pump room in three-unit pumping station on 24-inch pipe line

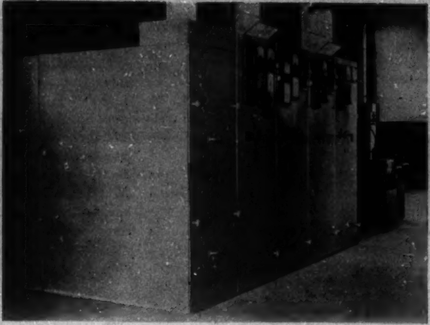


Figure 5. Metal-clad switchgear for full-voltage starting, in pumping station on 24-inch pipe line

are shut down automatically and successively at two-second intervals by timing relays. If the pressure recovers before all the motors are disconnected, further action is arrested automatically. Motors which have been shut down must be restarted by the operator, by push-button control.

If the station discharge pressure reaches 800 pounds, pressure regulators shut down the motors and prevent restarting until the pressure drops below 750 pounds.

All valves in the stations on the 24-inch line are manually operated, and each pump is stopped normally by push-button control.

Of the total of 23 auxiliary pump motors at various stations along the line, 20 are equipped with magnetic starters having current-limiting fuse protection, and the others with oil-circuit-breaker type starting equipment.

The stations on the 24-inch line handle 12.43 gallons of fluid per minute per square inch of floor space. This sets what is believed to be a new high record for efficient use of building area for this purpose, and makes these the smallest big pumping stations in the world. The motor room, 60.5 feet long by 20.8 feet wide, has a volume of 21,700 cubic feet in which motor heat losses of 575,000 Btu, or 26.5 Btu per cubic foot, are produced hourly that would result in a temperature rise of something on the order of 50 degrees Fahrenheit if not disposed of by the exhaust fans. However, for centrifugal-pump stations they are the most efficient so far built, as each pump unit has an over-all full-load efficiency of about 82 per cent.

POWER SUPPLY FOR 24-INCH LINE

Power is supplied by 16 public utilities. Not more than four stations are fed by any one system, and in eight cases only one station is supplied. High-line voltages are 22, 25, 26, 33, 34.5, 66, 69, and 110 kv. The typical station (Figure 7) has a 3,750-kva three-phase oil-insulated self-cooled transformer, with a secondary voltage of 2,400, located just outside the pumping station and protected by station-type lightning arresters. A bank of three single-phase 2,400–240/120-volt transformers, one of them 25 kva and the other two 15 kva, is mounted on the outdoor structure and furnishes power for lighting, auxiliaries, and camp service.

The outdoor substation is of creosoted-wood-pole construction to comply with War Production Board limitations in the use of steel.

Figure 4. Motor and control room in three-unit pumping station on 24-inch pipe line, showing 1,500 - horsepower 1,800-rpm 2,300-volt induction motors (left) and switchgear for full-voltage starting (right)

PUMPING STATIONS ON 20-INCH PIPE LINE

The 20-inch line, though longer than the 24-inch line, has the same number of pumping stations because split and stand-by stations have been eliminated. Where the two lines are parallel, station sites are in the same locations. There are a total of 86 1,250-horsepower 3,600-rpm main centrifugal pumps, all in series; also there are three 700-horsepower 3,600-rpm feeder pumps, two 1,500-horsepower 1,800-rpm, and one 400-horsepower 1,800-rpm, delivery pumps, and five 150-horsepower booster pumps on the 20-inch pipe-line project, as well as a sump pump and three fans at each station. The total connected load is approximately 114,500 horsepower, thus making a total of approximately 243,000 horsepower on both pipe lines.

The main pump speed of 3,600 rpm was selected because of its much higher efficiency in pumping gasoline, a fact which illustrates that the technique of pumping this product differs materially from that required for crude oil and explains why more elaborate station control is used. The pumps are 14 inch by 12 inch single stage, rated 6,855 gallons per minute at 825 feet head.

The 1,250-horsepower motors of the

general type shown in Figure 8 are similar to the 1,500-horsepower motors on the 24-inch line except that they are designed with normal starting current and provided with a forced-oil lubrication system built integrally with the motor and supplemented by oil rings.

The station arrangement on the 20-inch line (Figure 9) differs somewhat from that on the 24-inch line, as may be seen by comparison with Figure 2. A slight air pressure is maintained in the control room to exclude gasoline vapors, and a large sealed glass area in the partition gives the operator a view of both motor and pump rooms.

Operating pressures and voltages, the number of pumping units per station, the pump connections and by-passing arrangement, the switchgear and starting methods, and the transformers and other outdoor equipment on the 20-inch line are all the same as, or correspond closely to, those on the 24-inch line. The same number of power companies supply the power, the total number for the two projects being 19.

Automatic Features on 20-Inch Line

As gasoline requires much more care in pumping-station operation, numerous safeguards in the form of automatic protection and some features of automatic operation have been provided for the 20-inch line.

Inasmuch as it has been the policy of War Emergency Pipelines, Inc., to confine such features to essentials, no electrical interlocking has been provided between successive pumping stations along the line. Operations depending upon the action at the upstream or downstream stations are governed in accordance with the hydraulic suction and discharge conditions thus created at each station.

The electric control equipment necessary to provide the automatic functions is housed in a cubicle separate from the switchgear and located adjacent to it in the control room. All indicating lights are located on a schematic station diagram (Figure 10) incorporated in a control desk located in the control room where full view of the motor and pump rooms is obtained by the operator through the glassed partition. On the control desk are also mounted "start" and "stop" push buttons for each unit and an alarm cut-out button. Lighted arrows incorporated in the schematic station diagram show the path of the product through the station in accordance with the units in operation.

The control-desk assembly also provides a panel on which are mounted the suction- and discharge-pressure controllers, recorders and indicating gauges, air-pressure gauge and regulator-adjusting knob, and a clock.

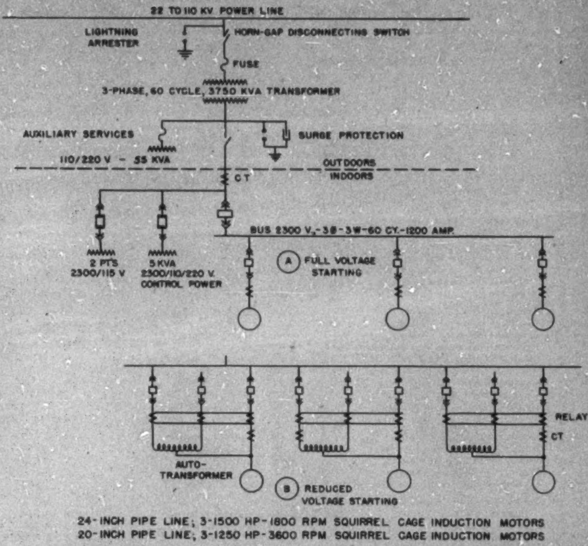

22 TO 110 KV POWER LINE
LIGHTNING ARRESTER
HORN-GAP DISCONNECTING SWITCH
FUSE
3-PHASE, 60 CYCLE, 3750 KVA TRANSFORMER
AUXILIARY SERVICES
SURGE PROTECTION
110/220 V - 55 KVA
OUTDOORS
INDOORS
CT
BUS 2300 V.-3Ø-3W-60 CY.-1200 AMP.
2 PTS 2300/115 V
5 KVA 2300/110/220 V. CONTROL POWER
A FULL VOLTAGE STARTING
RELAY
CT
AUTO-TRANSFORMER
B REDUCED VOLTAGE STARTING
24-INCH PIPE LINE; 3-1500 HP-1800 RPM SQUIRREL CAGE INDUCTION MOTORS
20-INCH PIPE LINE; 3-1250 HP-3600 RPM SQUIRREL CAGE INDUCTION MOTORS

Figure 6. Power diagram of typical three-unit pumping station on 24-inch and 20-inch pipe lines

The schematic wiring diagram (Figure 11) shows the automatic operating and protective circuits in a typical three-unit station.

Location of Operator's Control Stations, 20-Inch Pipe Line

The incoming-line breaker is operated by a manual control switch (device 11) located on the incoming-line panel. It is used to control all normal opening and closing operations of the breaker.

For each of the three main pump motors there are three locations from which manual-control operations can be performed:

1. Push buttons (devices 102, 202, and 302) for motor starting and stopping are located on the control desk.

2. Control switches (devices 101, 201, and 301) are located on the motor-switchgear panels and have two operating positions, "trip" and "remote." The "remote" position provides permissive starting through the "start" push buttons (devices 102, 202, and 302) located on the control desk. When the control switch is in the "trip" position, remote closing of the motor-starting breaker is prevented.

3. "Stop-run" push-button stations (devices 103, 203, and 303) located in the pump room are used to shut down the corresponding pump unit. The unit cannot be restarted from this position; the push button must be in the "run" position before the unit can be restarted at the control desk.

Normal Operation, 20-Inch Pipe-Line Stations

Each pump unit has motor-operated suction and discharge valves with limit switches, and is started separately by the pushing of a button at the control desk by the operator. Provided that no protective device associated with the unit is registering an abnormal condition, this starts the ventilating-fan motor and the opening of the pump suction valve. At the end of the valve travel, the switchgear is energized automatically to start the pump motor. When the running breaker on the motor starter closes, this starts automatic opening of the pump discharge valve. Valves of the plug type are used, with sufficient overlap so that the motor is practically up to full speed before the discharge valve is open materially.

The motor cannot start unless the suction valve is fully open and the discharge valve fully closed. If the suction valve leaves its fully open position because of any abnormal condition while

Table I. Miscellaneous Data for Transmission-Line Extensions and Transformer Substations

Pump Station Number	Utility Services and Extensions*			New Conductors		Transformer (Rated Kva and Connection)**
	Company Designation	Line (Kv)	Length (Miles)	Material and Size**	Spacing (Equivalent Feet)	
1	Southwestern Gas and Electric Co.	66	0.65	Steel, 0.5 inch	8.5	1—3,750
2	Southwestern Gas and Electric Co.	66	2.40	Steel, HTC-130	8.5	1—3,750
3	Arkansas Power and Light Co.	110	7.06	Steel, 0.5 inch	18.0	1—3,750
4	Arkansas Power and Light Co.	110	11.55	Steel, 0.5 inch	18.0	1—3,750 Y
A	Houston Light and Power Co.	66	3.00	Steel, 0.5 inch	8.5	1—2,000
B	Gulf States Utilities Co.	33		Line tap (100 feet long)		1—3,750
C	Gulf States Utilities Co.	66	17.00	Steel, 0.5 inch	8.5	1—3,750
D	Louisiana Power and Light Co.	110	55.00	Steel, 0.5 inch	18.0	1—3,750 Y
E	Louisiana Power and Light Co.	110	25.00	Steel, 0.5 inch	18.0	1—3,750 Y
F	Arkansas Power and Light Co.	110	6.00	Steel, 0.5 inch	18.0	1—3,750 Y
G	Arkansas Power and Light Co.	110	3.00	Steel, 0.5 inch	18.0	1—3,750 Y
5	Arkansas Power and Light Co.	110	0.10	Steel, 0.5 inch	18.0	2—3,750 Y
6	Arkansas Power and Light Co.	110	1.66	Steel, HTC-130	18.0	2—3,750 Y
7	Arkansas-Missouri Power Corp.	110	4.48*	Steel, $^3/_8$ inch*	18.0	2—3,750
8	Arkansas-Missouri Power Corp.	110	48.40	(a) Cu W	18.0	2—3,750 Y
9*A*	Missouri Utilities Co.	33	0.07	Steel, 0.5 inch	4.5	1—1,500
9	Arkansas-Missouri Power Corp.	110	54.95	(b) ACSR	18.0	1—5,000 Y
9*B*	Central Illinois Public Service	66	18.21	Steel, 0.5 inch	8.5	1—2,500
10	Central Illinois Public Service	66	8.85	Steel, HTC-130	8.5	2—3,750
11	Central Illinois Public Service	66	0.10	Steel, 0.5 inch	8.5	2—3,750 1—500
12	Public Service Co. of Indiana	$34^1/_2$	3.00	Copper number 4		2—3,750
13	Public Service Co. of Indiana	$34^1/_2$	7.00	Copper number 2		2—3,750
14	Public Service Co. of Indiana	69	1.00	Copper 2/0		2—3,750
15	Public Service Co. of Indiana	$34^1/_2$	3.00	Copper number 4		2—3,750
16	Cincinnati Gas and Electric Co.	66	8,00	HDBC number 2		2—3,750
17	Columbus and Southern Ohio Electric Co.	69	16.49	Cu Cw $^3/_8$ inch		2—3,750 Y
18	Ohio Power Co.	69	1.40	ACSR number 2	6.95	2—3,750 Y
19	Ohio Power Co.	69	16.00	ACSR number 2	6.95	2—3,750 Y
20	West Penn Power Co.	25	12.00	Copper 1/0		2—3,750
21	West Penn Power Co.	25	2.00	Copper 1/0		2—3,750
22	Pennsylvania Electric Co.	22	3.50	Copper 2/0		2—3,750
23	Southern Pennsylvania Power Co.	69	0.50	Copper 1/0		2—3,750
24	Pennsylvania Power and Light Co.	66	4.50	Copper number 4		2—3,750
25	Philadelphia Electric Co.	33	10.00	Cu Cw-1/0		2—3,750
Emergency *A*	Philadelphia Electric Co.	33				1—3,750 S
26	New Jersey Power and Light Co.	33	1.00	ACSR-3/0		2—3,750
Emergency *B*	Public Service Electric and Gas Co.	26				1—3,750 S

* Power is supplied to pump stations 7 and 8 by a common feeder using 12-strand, 2/0 type-*F*, 40 per cent Copperweld conductors for a distance of 9.44 miles to the pipe-line right of way, where it is connected to a sectionalized line running 4.48 miles in one direction to pump station 7 and 48.40 miles in the opposite direction to pump station 8.

** Abbreviations signify as follows: HTC-130, high-tension cable-130 strands; HDBC, hard-drawn bare copper; Cu W, Copperweld; Cu CW, copper-Copperweld; ACSR, aluminum conductor, steel-reinforced; and S, special connections for several high voltages.

(a). Conductors of this line consist of 7-strand, 2/0 type-*F*, 40 per cent Copperweld for 3.57 miles; $^1/_2$-in. 40 per cent Copperweld for 36.55 miles; and type V (0.522-inch diameter) 30 per cent Copperweld for 8.28 miles.

(b). Conductors of this line extension from pump station 8 to 9 consist of 2/0 steel-reinforced aluminum for 10.29 miles and 1/0 steel-reinforced aluminum for 44.66 miles. The latter size of conductor is minimum to prevent excessive corona losses.

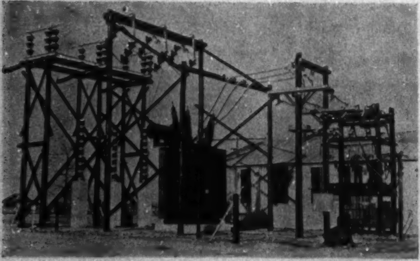


Figure 7. Typical outdoor substation on 24-inch pipe line, showing 110,000-volt fused disconnecting switches and lightning arresters (left) and three-phase power transformer and 2,400-volt circuits with auxiliary low-voltage transformers (right)

the pump is in operation, the unit is automatically stopped.

Normal stopping of each of the pump units is accomplished by pushing a button. In case of either normal or automatic abnormal shutdown, the associated ventilating fan is automatically stopped simultaneously and the valves travel automatically and at once to their closed position. In case of power interruption causing the motor switchgear to trip, the valves return automatically to their closed position upon resumption of normal voltage.

AUTOMATIC PROTECTIVE FEATURES IN 20-INCH-LINE STATIONS

The protection provided is as follows:

(a). High motor-stator temperature, low air pressure, and high sump level will each result in sounding an alarm and lighting a red lamp indicating where the trouble is located.

(b). Incoming power or motor overcurrent, high motor or pump-bearing temperature, high pump pressure or casing temperature, low station suction head, and high line discharge pressure will each not only sound the alarm and light the corresponding indicating lamp, but will trip the corresponding circuit breaker. High pump pressure, furthermore, will result in a sequential tripping action further described.

(c). Undervoltage will result only in a time-delay tripping of breakers.

When the high-pump-pressure protective switch functions it immediately shuts down the pump on the downstream side of the station, and if this does not relieve the condition, the next unit upstream in the station is shut down automatically after a time delay, adjustable from one-half to four seconds. If this still does not relieve the condition, the third unit is shut down automatically after a similar time delay.

Station suction and downstream line pressures are controlled by air-operated regulators, and if the suction head becomes too low or the line pressure on the discharge side too high, the entire station is shut down automatically, and check valves function to by-pass the station. Any individual pump which is shut down is likewise by-passed by a check valve.

Adequate Relay Protection

Many engineers think of two-coil overload relay protection as being adequate for three-phase motors on nongrounded distribution systems. This is so when the incoming-power transformer is protected by a breaker, but not necessarily so if it is protected by high-voltage fuses. Delta–wye- or wye–delta-connected transformers, fused on the primary side and having nongrounded neutrals, are exceptions to the general rule, as may be seen by referring to Figure 12. These exceptions may warrant the use of three-coil overload relay protection or its equivalent for motors, especially for motors as large as those being used by War Emergency Pipelines, Inc.

Wye–delta-connected transformers having substantially grounded neutrals should always be protected by breakers controlled by suitable relays. However, two-coil overload relay protection is sufficient for motors connected to the secondary circuit of such transformers.

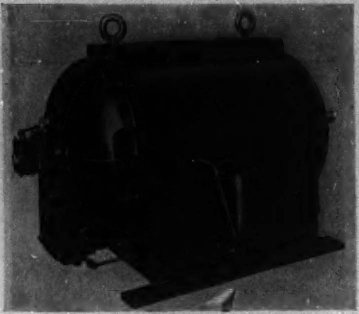


Figure 8 (left). 1,250-horsepower 3,600-rpm 2,300-volt induction motor for main pump drive on 20-inch pipe line

It may be economically undesirable to require three overcurrent relays in each motor circuit for small motors even though they receive power from a delta–wye- or wye–delta-connected transformer, as circumstances seldom may occur to cause a motor burnout owing to transformer connections. However, large and expensive motors warrant more adequate protection, especially if they are in pipeline pumping service or other load-sequencing operations.

Some of the 19 power companies supplying power to these projects recommended the use of wye–delta-connected transformers having nongrounded neutrals, and all of them are fused on the high-voltage side. The single-phase operating condition would cause twice as much current in one motor-phase conductor as in the other two. This condition means that two-coil overload relay protection is inadequate, irrespective of whether the motor is connected wye or delta. War Emergency Pipelines, Inc., added a current-balance relay to the incoming 2,400-volt bus breaker equipment to provide the equivalent of three-coil overload relay protection at these locations.

Adequate Power Supply

The choice of a suitable right of way for the 24-inch crude oil and 20-inch products pipe lines was based on many factors, and one of the more important ones was the availability of power for urgent projects of this nature. Availability of central-station power along the pipe-line right of way made this source of power the most attractive one from the standpoint of over-all economy in time, critical materials, and capital investment. Furthermore, it is understood that purchased power rates for these high-load-factor projects are competitive with power generated on the right of way. Some preliminary calculations indicate that this is especially so for partial-load operation, such as two motors per pump station.

A typical pump-station site includes two pump stations; one for the 24-inch line and one for the 20-inch line. Their incoming-power services are practically identical in that each station receives power through a 3,750-kva three-phase oil-insulated self-cooled transformer, with provision for mounting fans at a later date to increase the maximum output to 5,000-kva if necessary. Calculations show that the inputs to each transformer will be approximately 3,600-kw for the 24-inch line and approximately 3,000-kw for the 20-inch line. This gives a total load concentration of 6,600-kw at each pump-station site, with a power-factor for this load in the range of 87 to 90 per cent on the high-voltage side of each transformer. These figures include power for auxiliary services. The total load of 6,600-kw represents an average transmission-line loading of 125-kw per mile of pipe line on the common right of way for the two pipe lines.

The problem of designing the high-voltage line extensions to serve this load was essentially one of doing it with available materials yet making them adequate to carry the load of all of the motors at each pump-station site, with one of them being started without introducing too much voltage drop. Only one high-voltage circuit extension is being provided for each pump-station site. Creosoted-wood-pole structures were used as much as practicable in these high-voltage line extensions and transformer substations at each pump-station site (see Figure 7).

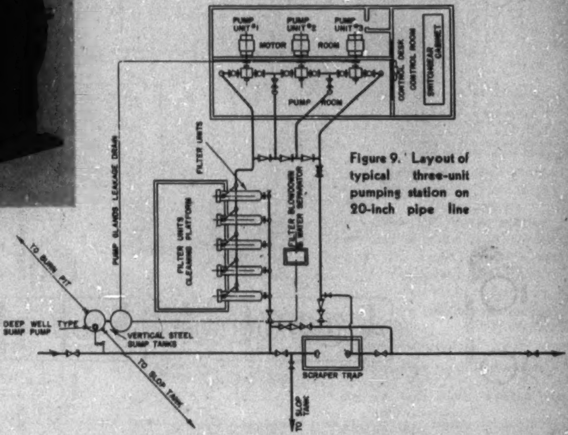


Figure 9. Layout of typical three-unit pumping station on 20-inch pipe line

Critical materials were saved as much as practicable by the substitution of wood construction for steel, and steel-wire conductors for copper and aluminum, where feasible. Table I gives a partial list of information pertaining to transmission-line extensions and transformer substations.

Common practice in oil fields is to place boilers at least 150 feet away from the oil well being served so as to minimize the fire hazard resulting from gas from the well. The original intention in designing the pipe-line stations was to use a similar means of protection in

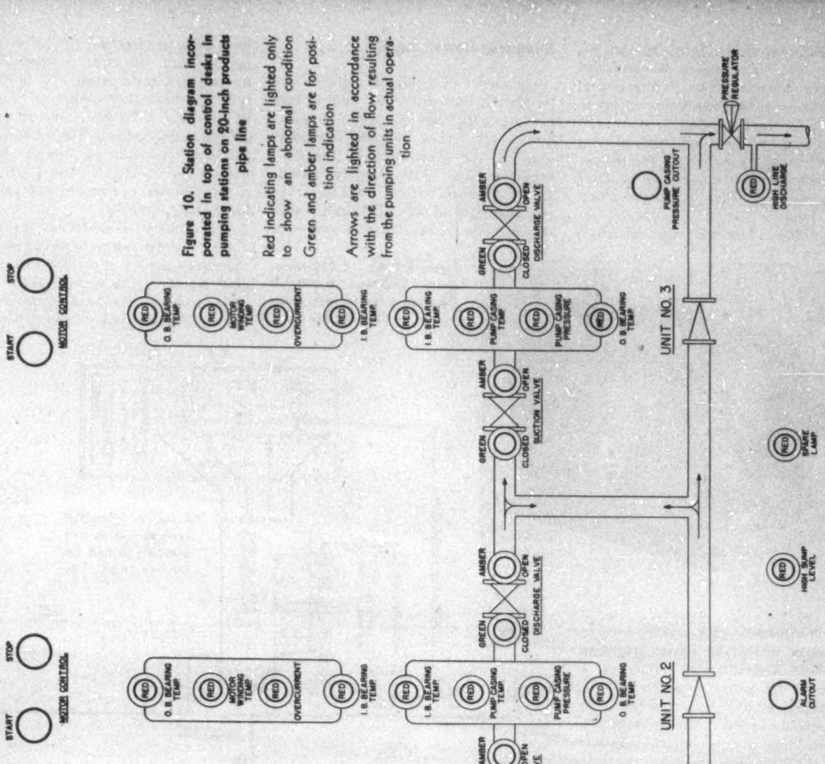

START
STOP
MOTOR CONTROL
RED
O. B. BEARING TEMP.
MOTOR WINDING TEMP.
OVERCURRENT
I. B. BEARING TEMP.
PUMP CASING TEMP.
PUMP CASING PRESSURE
GREEN
AMBER
CLOSED
OPEN
SUCTION VALVE
DISCHARGE VALVE
UNIT NO. 1
UNIT NO. 2
UNIT NO. 3
LOW SUCTION PRESSURE
INCOMING LINE OVERCURRENT
LOW AIR PRESSURE
ALARM CUTOUT
HIGH SUMP LEVEL
SPARE LAMP
PUMP CASING PRESSURE CUTOUT
HIGH LINE DISCHARGE
PRESSURE REGULATOR

Figure 10. Station diagram incorporated in top of control desks in pumping stations on 20-inch products pipe line

Red indicating lamps are lighted only to show an abnormal condition

Green and amber lamps are for position indication

Arrows are lighted in accordance with the direction of flow resulting from the pumping units in actual operation

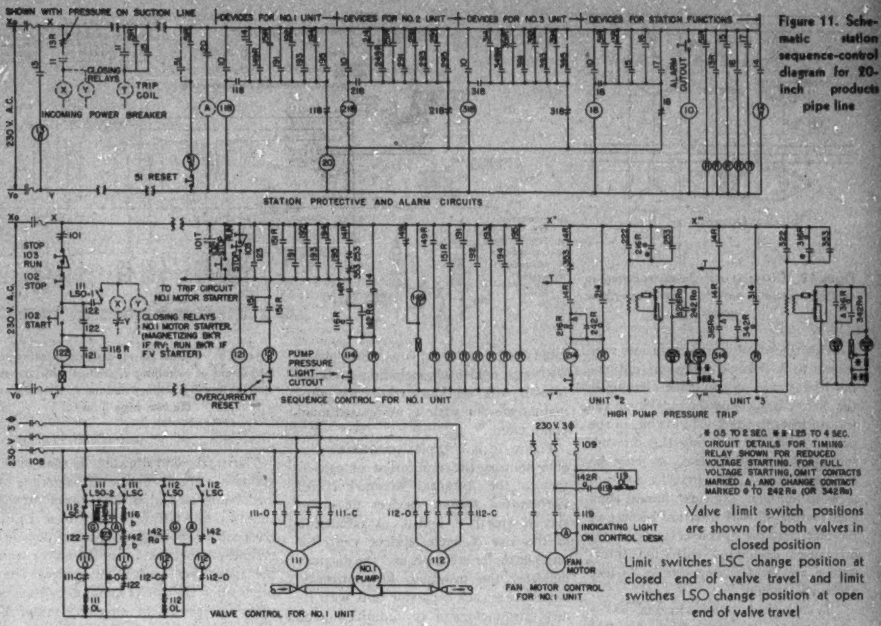

SHOWN WITH PRESSURE ON SUCTION LINE
DEVICES FOR NO.1 UNIT
DEVICES FOR NO.2 UNIT
DEVICES FOR NO.3 UNIT
DEVICES FOR STATION FUNCTIONS
CLOSING RELAYS
TRIP COIL
INCOMING POWER BREAKER
230 V. A.C.
51 RESET
ALARM CUTOUT
STATION PROTECTIVE AND ALARM CIRCUITS
STOP 103 RUN
102 STOP
102 START
TO TRIP CIRCUIT NO.1 MOTOR STARTER
CLOSING RELAYS NO.1 MOTOR STARTER (MAGNETIZING BK'R IF RV; RUN BK'R IF FV STARTER)
PUMP PRESSURE LIGHT CUTOUT
OVERCURRENT RESET
SEQUENCE CONTROL FOR NO.1 UNIT
UNIT #2
UNIT #3
HIGH PUMP PRESSURE TRIP
230 V 3Φ
108
VALVE CONTROL FOR NO.1 UNIT
NO.1 PUMP
230 V. 3Φ
109
INDICATING LIGHT ON CONTROL DESK
FAN MOTOR
FAN MOTOR CONTROL FOR NO.1 UNIT
# 0.5 TO 2 SEC. ## 1.25 TO 4 SEC.
CIRCUIT DETAILS FOR TIMING RELAY SHOWN FOR REDUCED VOLTAGE STARTING. FOR FULL VOLTAGE STARTING, OMIT CONTACTS MARKED A, AND CHANGE CONTACT MARKED B TO 242Ra (OR 342Ra)

Figure 11. Schematic station sequence-control diagram for 20-inch products pipe line

Valve limit switch positions are shown for both valves in closed position

Limit switches LSC change position at closed end of valve travel and limit switches LSO change position at open end of valve travel

A	audible alarm
Ⓐ	amber indicating light
Ⓖ	green indicating light
Ⓡ	red indicating light
T	trip coil for breaker
X	breaker-closing relay
Y	auxiliary breaker-closing relay
10	alarm cutout relay
11	control switch, incoming line
13	low suction head protective switch
13R	auxiliary relay for 13
14	high pump pressure protective switch
14R	auxiliary relay for 14
15	high-line discharge pressure protective switch
16	low air pressure protective switch
17	high sump level protective switch
18, 118, 218, 318	auxiliary alarm relays
20	alarm relay
51, 151, 251, 351	overcurrent relays
51R, 151R 251R, 351R	auxiliary relays for overcurrent trip
51 reset, 151 reset 251 reset, 351 reset	"reset" buttons for overcurrent relays
101, 201, 301	motor "trip-remote" switches
102, 202, 302	motor "start-stop" buttons
103, 203, 303	motor "start-run" buttons
108, 208, 308	valve-motor breakers
109, 209, 309	fan-motor breakers
111, 211, 311	suction-valve motors
111-C, 211-C, 311-C 111-O, 211-O, 311-O	suction-valve reversing switches
111-LSC, 211-LSC 311-LSC	suction-valve limit switches for closed-valve limit
111-LSO, 211-LSO 311-LSO	suction-valve limit switches for open-valve limit
112, 212, 312	discharge-valve motors
112-C, 212-C, 312-C 112-O, 212-O, 312-O	discharge-valve reversing switches
112-LSC, 212-LSC 312-LSC	discharge-valve limit switches for closed-valve limit
112-LSO, 212-LSO 312-LSO	discharge-valve limit switches for open-valve limit
114, 214, 314	auxiliary relays for high pump pressure
116R, 216R, 316R	position relays for motor starting breakers
119, 219, 319	fan-starting switches
121, 221, 321	lockout relays
122, 222, 322	sequence relays for valves and fans
123, 223, 323	valve sequence relays
142R, 242R, 342R	position relays for motor-running breakers
149, 249, 349	motor-stator temperature protective switches
149R, 249R, 349R	auxiliary relays for 149, et cetera
191, 291, 391 192, 292, 392	motor-bearing temperature protective switches
193, 293, 393 194, 294, 394	pump-bearing temperature protective switches
195, 295, 395	pump-casing temperature protective switches
216R, 316R	auxiliary contacts on motor-starting breakers
242R, 342R	auxiliary contacts on motor-running breakers
253, 353	sequence trip timing relays for high pump pressure

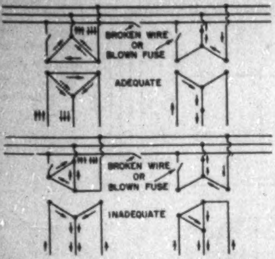

BROKEN WIRE OR BLOWN FUSE
ADEQUATE
BROKEN WIRE OR BLOWN FUSE
INADEQUATE

Figure 12. Diagrams of single-phase current in multiple circuits provided by three-phase motors

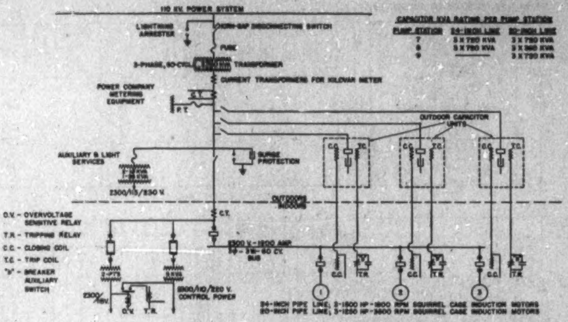

110 KV. POWER SYSTEM
LIGHTNING ARRESTER
HORN-GAP DISCONNECTING SWITCH
FUSE
3-PHASE, 60-CYCL.
TRANSFORMER
CURRENT TRANSFORMERS FOR KILOVAR METER
POWER COMPANY METERING EQUIPMENT
C.T.
P.T.
CAPACITOR KVA RATING PER PUMP STATION
PUMP STATION
24-INCH LINE
20-INCH LINE
7
3 X 720 KVA
3 X 720 KVA
8
3 X 720 KVA
3 X 360 KVA
9
3 X 720 KVA
OUTDOOR CAPACITOR UNITS
C.C.
T.C.
AUXILIARY & LIGHT SERVICES
SURGE PROTECTION
2300/115/230 V.
OUTDOORS
INDOORS
C.T.
O.V. - OVERVOLTAGE SENSITIVE RELAY
T.R. - TRIPPING RELAY
C.C. - CLOSING COIL
T.C. - TRIP COIL
"b" - BREAKER AUXILIARY SWITCH
2300 V - 1200 AMP
3φ - 3W - 60 CY. BUS
2-PTS
2300/115V.
O.V.
T.R.
2300/110/220 V. CONTROL POWER
C.C.
T.R.
1
2
3
24-INCH PIPE LINE; 3-1500 HP-1800 RPM SQUIRREL CASE INDUCTION MOTORS
20-INCH PIPE LINE; 3-1250 HP-3600 RPM SQUIRREL CASE INDUCTION MOTORS

Figure 13. Typical power diagram showing arrangement of switching capacitors with associated motors in pumping stations 7, 8, and 9 on the two pipe lines

locating the transformer substations with respect to their associated pump stations, but it was necessary to make a saving in the heavy-copper conductors from the transformers to the 2,400-volt bus in the pump station by reducing this distance to approximately 25 feet, as may be seen in Figure 7. This practice does not seem to represent a greater hazard than that for the oil-well situation because of the fact that the disconnecting switches and fuses associated with the transformer are located from 20 to 30 feet above the ground.

The arrangements for adequate power supply to all pump stations are expected to provide successful operation without further additions except in two areas; one being served by the West Penn Power Company, and the other being served by the Arkansas–Missouri Power Corporation. Capacitors are being added to the 2,400-volt terminals of the main power transformers at pump stations 7, 8, 9, and 20 to obtain a satisfactory voltage level at these locations. These capacitors are to be switched automatically with their associated motors.

The West Penn Power Company found it necessary to strengthen their power supply by adding 7.5 miles of new circuit to the 132-kv system feeding the 25-kv substations, from which line extensions shown in Table I supply power to pump stations 20 and 21 on both pipe lines. It was necessary, furthermore, to add capacitors at both pump stations at pump-station site 20. There are six of these outdoor capacitor units, each being rated 360 kva at 2,400 volts, three phase, 60 cycles, and each including a circuit breaker with necessary relays for automatic operation with its associated main pump motor.

Figure 13 is a typical schematic diagram showing the application of capacitors to the Arkansas–Missouri Power Corporation system services at pump station sites 7, 8, and 9. A tabulation of the size of each outdoor capacitor unit per motor is shown, and the diagram indicates the provision for automatic switching of each capacitor unit with its associated motor so as to minimize voltage variations on the system. Studies made by the power company determined that these sizes of capacitor units are needed in this area to maintain system voltage at this point of their system, which was designed for a maximum load of about 8,000 kw but which must carry now the additional pipe-line load of approximately 18,000 kw. It is understood that the Arkansas Power and Light Company has a postwar project for a new power plant in this general area, a fact that should make it unnecessary to continue the use of these capacitors after the plant is completed.

These capacitor units are much larger than those normally associated with individual motors of 1,250- and 1,500-horsepower, and it was advisable, therefore, to investigate and provide protection against the possibility of overvoltages that could be caused by the capacitors if the 2,400-volt system of the pump station became isolated from the source of power. This could occur if two high-voltage fuses should blow simultaneously, although the possibility of such an occurrence is rather remote. Provision for protection was made, as shown in Figure 13, by using sensitive overvoltage relay equipment for tripping the capacitor breakers.

The extent and duration of such overvoltages depend upon the amount of load being carried by the motor at the time of power failure. The rate at which the peak overvoltage is reached depends upon the time constant of the motor, and the magnitude of the overvoltage depends upon the rate of motor deceleration when the source of incoming power is disconnected. Data obtained indicate that such overvoltages will prevail for less than two seconds under expected operating conditions.

Calculations were based upon the connection of three 1,500-horsepower 1,800-rpm 2,300-volt motors to one 3,750-kva 110/2.4-kv transformer, and were made by determining the magnetization curve of one motor and the transformer for overvoltage conditions, and combining that information with the discharge rate of the capacitors. The capacitor discharge absorbed by transformer excitation reduces the maximum overvoltage to be expected at the motor terminals by 15 to 20 per cent. Calculated results without load on the motor are approximately as listed in Table II. These voltage percentages allow for the

Table II

Capacitor Kva Per Motor	Per Cent Voltage
360	115
720	137
1,080	153

Table III

Motor Load (Per Cent)	Maximum Voltage (Per Cent)	Overvoltage Duration in Seconds Above Bus Voltage, Without Relay Protection
0	164.0	13.0
10	160.0	10.0
20	156.0	7.5
30	152.0	5.0
40	148.0	3.5
50	144.0	2.5
75	134.5	1.0
100	124.5	0.5

effect of the transformer magnetizing kilovolt-amperes.

Factory tests were made by connecting 720 kva in capacitors across the terminals of one of the 1,500-horsepower motors, from which the approximate data listed in Table III were obtained. The sensitive overvoltage relay equipment was used to trip the capacitor breaker under the no-load condition on the motor, and the voltage rise was not appreciably greater than the relay settings used in making the tests, thus demonstrating the effectiveness of the relay for preventing excessive overvoltages.

Emergency Engineering

It is remarkable that these urgent projects were carried forward by War Emergency Pipelines, Inc., so rapidly that the 24-inch crude-oil line was completed without having in advance full engineering information on the co-ordination of pipe-line electrical equipment with the transmission system of the 16 power companies involved. Of all the early engineering decisions made, only one of them seems to warrant being mentioned here. Transformers for pump stations 7 and 8 were purchased five months before engineering studies for this area were completed by the power company, but the transformer taps chosen still provide voltage at the motor terminals within guaranteed limits without injury to the motor insulation, even though the motors are operated at full load continuously. These engineering studies were completed before transformers were purchased for the 20-inch products line, so it was then practicable to determine the proper taps for the transformers, namely, four five per cent taps below normal-rated voltage at pump station sites 7, 8, and 9. Table IV shows expected operating conditions at these stations. The voltage at the motor terminals can be improved readily in pump stations 7 and 8 of the 24-inch pipe line by adding booster transformers on the 2,400-volt side of the power transformer. It is believed that the data in Table IV make an interesting example of noteworthy results of emergency engineering under the conditions required to expedite the construction and operation of the "Big Inch" and "Little Big Inch" projects.

In view of the urgency and size of these projects, War Emergency Pipelines, Inc., distributed their purchases of electrical and other equipment among several manufacturers, all of whom co-operated to produce interchangeability of their products where needed and rendered engineering assistance to the pipe-line organization in many ways. The foremost thoughts and efforts of all concerned were devoted to hastening the work and, thus, to assisting in the alleviation of the critical oil situation in the eastern states and in the supplying of the Armed Forces with oil products which they need. Among the several men in the organization of War Emergency Pipelines, Inc., who were associated with the electrification, special mention is deserved by W. H. Stueve, Oklahoma City, Okla., who acted as power consultant and who, in that capacity, negotiated the power agreements with the 19 power companies involved. His advice on engineering problems connected with distribution of power to pipe-line pumping stations was of very great value to the projects.

Table IV. Capacitors Switched With Motors

Calculated Electrical Conditions	Pipe-Line Pump Stations					War Emergency Pipe-lines Junction Total	Waina Ridge Sub-station
	9A	8		7			
	20 Inch and 24 Inch	20 Inch	24 Inch	20 Inch	24 Inch		
Normal Full-Load Operation							
Motor voltage	2,300	2,350	2,110	2,410	2,060		
Capacitor rated kva	2,160	1,080	2,160	2,160	2,160	9,720	12,240
Capacitor net kva	1,985	1,035	1,670	2,180	1,590	8,460	10,980
Transformer per cent tap	20	20	15	20	10		
Transformer per cent amperes	80.5	76.5	97.0	75.0	100.0		
Station load kw	4,120	2,940	3,540	2,940	3,540		
Kilovars lagging		225					
Kilovars leading	210		115	920	45		
High-voltage line kv	91.3	92.7		95.0		95.8	96.2
High-voltage line amperes	27.9	70.6		39.9		112.0	114.0
Total load kw	4,165	10,825		6,550		18,025	18,190
Kilovars leading	1,415	3,260		325		4,410	5,270
Line per cent power factor leading	94.7	95.7		99.9		97.1	96.0
Load With Full-Voltage Starting of 1,500-Horsepower Motor in Station 9A—							
Motor voltage	1,870	2,150	1,910	2,325	1,975		
Capacitors net kva	1,310	870	1,370	2,030	1,460	7,040	9,580
Transformer per cent I	161.0	84.0	107.0	77.0	104.0		
Station load kw	3,765	2,940	3,540	2,940	3,540		
Kilovars lagging	5,020	390	185		90		
Kilovars leading				770			
High-voltage line kv	80.7	85.7		92.0		92.85	93.9
High-voltage line amperes	43.3	76.0		41.3		113.5	114.0
Total load kw	3,870	10,690		6,565		18,010	18,180
Kilovars lagging	4,660	3,580		5		2,880	2,260
Line per cent power factor lagging	63.8	94.7		Unity		98.9	99.3

Costs Study of 69- to 345-Kv Overhead Power-Transmission Systems

JOHN GRZYBOWSKI HOLM
MEMBER AIEE

Synopsis: With transmission voltages of long and heavy trunk lines having exceeded, in the case of the Boulder Dam line, the popular 230-kv voltage, it appears in order to investigate the cost of lines at a still higher voltage, particularly since the design and manufacture of the 345-kv equipment do not seem to present special difficulties. The study undertaken endeavors to find the economic relationship between voltage, block of power, and distance for the transmission of various blocks of power over distances up to 500 miles. The systems studied are made stable in the transient state by equipping them with devices which increase their transient-stability limit. The prices of equipment are those which were in force immediately prior to the outbreak of the second World War, and the costs of various system parts, as installed on foundations, have been obtained from the study of numerous actual construction projects in different parts of the country and reduced to the 1938–39 cost level. The system capital and annual costs are analyzed. The quantities of major equipment items in each system are given either by number or by weight, and the costs of major equipment groups and of items within a group are represented on a percentage basis. This representation permits the estimation of the cost of a transmission project based on cost of equipment and copper and steel prices other than those taken in the study. The cost of stability improvement is calculated, and this expenditure proves to be amply justified. A region defined by transmission distance and block of power is outlined, for which the transmission at 345 kv is more economical than when 230 kv are used. The curves given may be interpolated, and transmission-cost data may be obtained for a wide variety of systems at any basic price.

ABOUT 25 years ago when the highest transmission voltage in this country was approximately 160 kv, Silver analyzed the possibilities of 220-kv power transmission in a paper presented to the Institute.[1] Since then numerous 230-kv lines have been built, and in one case a 287-kv transmission line was constructed. In the meantime the question of power supply from local and distant steam-power stations, or from distant hydroelectric stations, was quite active, and papers on this subject were written by Fortescue,[2] Fowle,[3] and Funk[4] in this country, and by Rüdenberg[5] and Vögeli[6] in Europe. In every case the authors estimated the costs of transmission lines of various voltages and mileages, frequently making liberal assumptions and simplifications, especially in regard to line design and system-stability requirements. Until now, however, there has been no generally accepted view as to the economics of transmission lines at very high voltages, and particularly as to the relationship between the voltage, the block of power transmitted, and the transmission distance as these affect the total cost of the project. In view of this, and because of postwar construction projects and coming developments in the art of power transmission (such as d-c transmission), it seems appropriate that a cost study of a variety of transmission projects be made on a more or less uniform basis, with similar standards of requirements applied in all cases.

The transmission systems investigated in this paper have no specific geographic location and are treated as a purely research project. All prices taken are those prevailing in the 12-month period immediately preceding the second World War. All prices of equipment are averages obtained from various manufacturers. The costs of land, structures, substation secondary equipment, labor, and installation on foundation were obtained from various public-utility companies as well as from the literature. The data thus secured were carefully analyzed, weighted, and reduced to those of the aforementioned 12-month period, using appropriate labor indexes. Sound public-utility practices in cost estimating have been followed and recommendations of the Railway and Utility Commissioners[7] adhered to. Capital and annual costs are kept separately for each system. The costs of major groups within a system are calculated as a percentage of the total system cost, the percentage method being applied also to major items within a particular group. Since quantities of all major equipment parts are given in every system either by actual numbers or by weight, any variations that may be encountered in a particular project in the amount of equipment or in the costs of particular items may be easily introduced and the cost of the project rapidly estimated. Until the present time, however, the prices of basic materials—copper and steel—changed but little from those assumed in this study. And although the details of design and the actual costs of particular transmission projects considered may vary from those arrived at in this paper (depending to some extent on the locality), the relationship between the costs of particular systems will remain basically as given. The various cases considered permit the determination of the effect of different voltages on system cost. The most economical block of power and distance of transmission can also be determined.

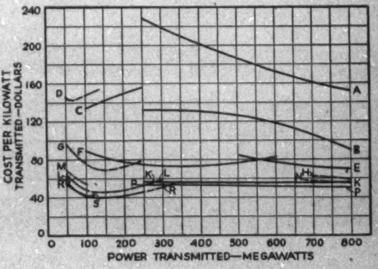
System Description

The 31 power-transmission systems studied transmit blocks of power of 50,000 to 800,000 kw (at the receiving end) over distances of 50 to 500 miles at sending-end voltages of 69, 138, 230, and 345 kv, at 60-cycle frequency. The power is transmitted from hydroelectric

Figure 1. Capital cost of the entire system per kilowatt transmitted

A.	500–345	K.	75–230
B.	250–345	L.	75–138
C.	250–230	M.	75– 69
D.	250–138	N.	50–345
E.	150–345	P.	50–230
F.	150–230	R.	50–138
G.	150–138	S.	50– 69
H.	75–345		

(First figure represents transmission distance in miles; the second, kilovolts of sending-end voltage)



Paper 44-62, recommended by the AIEE committee on power transmission and distribution for presentation at the AIEE winter technical meeting, New York, N. Y., January 24–28, 1944. Manuscript submitted November 22, 1943; made available for printing December 17, 1943.

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To the engineers of the General Electric Company, the Westinghouse Electric and Manufacturing Company, and the General Cable Corporation, and to several electric-public-utility, steel-construction, and consulting engineers the author expresses appreciation for valuable data and advice.

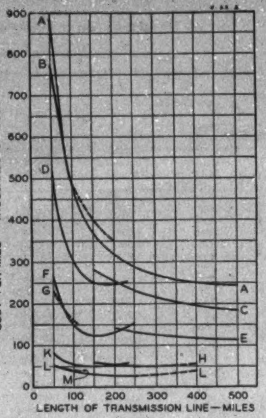


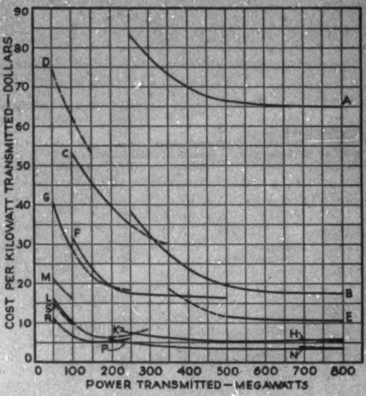
Figure 2. Capital cost of the entire system per mile

A.	800,000–345	*G.*	250,000–138
B.	800,000–230	*H.*	100,000–230
C.	500,000–345	*K.*	100,000–138
D.	500,000–230	*L.*	50,000–138
E.	250,000–345	*M.*	50,000– 69
F.	250,000–230		

(First figure represents kilowatts transmitted, the second, kilovolts of sending-end voltage)

Figure 3. Capital cost of the transmission line per kilowatt transmitted

Cost of the right of way is not included in the cost of the transmission line. For explanation of identification letters, see Figure 1



plants to distant metropolitan load centers. All systems have a high degree of reliability and security of operation. They were represented on the a-c network analyzer and their stability characteristics determined. The system transient-stability limits are brought up to the 100 per cent rating which the systems deliver fully under a double line-to-ground fault at the sending-end bus, cleared in 10.5 cycles, with a subsequent loss of a circuit or a section of it. All data pertaining to system layout, design, and characteristics are given in another paper by the present author.[8] The cost of the various means applied because of the stability requirement is fully accounted for.

The systems as comprised in this study include the sending-end substation, the transmission line, the intermediate-condenser and sectionalizing substations, the right of way, and the receiving-end substation. The latter substation contains synchronous condensers and the system receiving-end bus whose voltage is the primary distribution voltage of the load center. The substations are of the outdoor type. All systems have high-voltage busses at both ends, the heavier systems having double busses. For every system the oil-circuit-breaker interrupting capacity is calculated according to the accepted procedure for the circuit opening time as determined on the network analyzer. All transmission lines are protected by two ground wires, with additional ground-wire protection of all substations. The systems have a single continuous-type tower-to-tower counterpoise, and the end-substation equipment is protected by lightning arresters. All systems have carrier-current equipment for both communication and relaying, the details of which are discussed in the appendix. Systems transmitting 250,000 to 800,000 kw have one spare generator unit, and one spare three-phase transformer bank when the number of transformer banks in the system is not over four. Where a larger number of transformer banks are installed, the question of spare capacity is solved by requiring of all transformers a 25 per cent overload for two hours. Systems transmitting 50,000 and 100,000 kw have one spare single-phase transformer unit.

Supplementary details on system equipment affecting the system costs are given in Table I.

Method of Estimating System Capital Cost

For the purpose of estimating the capital cost, the system is divided into the following six groups:

1. Equipment comparative increment cost.
2. The sending-end substation.
3. The transmission line.
4. The right of way.
5. All the intermediate-condenser and sectionalizing substations.
6. The receiving-end substation.

The group called "equipment comparative increment cost" contains certain system parts not belonging to the transmission line proper but which result from requirements imposed on the operation of the line and consequently must be accounted for. Certain pairs of systems transmitting equal blocks of power over equal distances at different voltages have generators of different design, because of the fact that the stability condition must be met. The system operating at the lower of the two voltages might often need generators of lower transient reactance, with damper windings, higher short-circuit ratio, and greater inertia. The difference in the cost of generators of these two systems is one of the items included in the "equipment comparative increment cost" group. In systems where shunt reactors are placed on the generator bus, the cost of these reactors, the cost of the larger kilovolt-ampere generator capacity required by them, the cost of the increment in water turbines necessary to carry the losses in these generators, the cost of the power-house superstructure for these generators and turbines, and the cost of the increase in size of the sending-substation structure needed to house the shunt reactors are other items included in this group. Thus the "equipment comparative increment cost" group contains items originating within the transmission line but grouped separately merely for the purpose of clarity.

The various subdivisions of the remaining five groups forming the system capital cost structure are given in the appendix.

The capital cost of each group is arrived at in the following fashion: The cost of all equipment (material) is obtained from the manufacturers. In cases where the cost

Table I. List of Systems and

For Other Major Equipment

System Identification Number	Block of Power Transmitted to Receiving End (Kw)	Transmission Distance (Miles)	Transmission Voltage at Sending End (Kv)	Number of Circuits in System	Transformers With Intermediate Synchronous Condensers: Number of Banks	Transformers With Intermediate Synchronous Condensers: Kva Per Three-Phase Bank	Transformers With Intermediate Synchronous Condensers: Voltage (Kv)	Conductors: Type*	Conductors: Cross Section (Thousand Circular Mils)	Conductors: Total Weight (Thousand Pounds)	Insulators, Total Number of Units in System
0-1a	50,000	50	69	2				*CSC*	250	1,247	11,200
0-2a	50,000	50	138	2				*HH*	138	687.5	19,800
0-3a	50,000	75	69	2				*CSC*	350	2,620	16,700
0-4a	50,000	75	138	2				*HH*	138	1,031	29,800
0-5a	50,000	150	138	2				*CSC*	250	3,742	59,600
0-6a	50,000	250	138	2	1	26,300	135/13.8	*CSC*	300	7,470	99,300
0-7a	100,000	50	138	2				*HH*	212	1,058	19,800
0-8a	100,000	75	138	2				*CSC*	250	1,871	29,800
0-9a	100,000	150	138	2				*CSC*	400	5,990	59,600
0-10a	100,000	150	230	2				*HH*	265	3,967	89,200
0-11a	100,000	250	230	2	1	89,500	225/16.5	*HH*	265	6,605	148,600
0-12a	250,000	50	138	2				*CSC*	850	4,240	19,800
0-13a	250,000	50	230	2				*HH*	316	1,575	29,800
0-14a	250,000	75	230	2				*HH*	316	2,362.5	44,700
0-15a	250,000	150	230	2				*HH*	400	5,999	89,200
0-16a	250,000	250	230	2	4	112,000	225/16.5	*CSC*	650	16,220	148,600
0-17a	250,000	250	345	2	2	85,000	337.5/13.8	*HH*	505	12,600	205,300
0-18a	250,000	500	345	2	4	81,000	340 and 335/16.5	*HH*	587	29,230	410,700
					4	82,750					
0-19a	500,000	50	230	3				*HH*	316	2,362.5	44,700
0-20a	500,000	75	230	3				*HH*	316	3,542	67,000
0-22a	500,000	150	230	3				*CSC*	700	15,700	133,900
0-23a	500,000	150	345	2				*HH*	505	7,560	123,300
0-24a	500,000	250	345	2	8	97,500	337.5/16.5	*HH*	505	12,600	205,300
0-25a	500,000	500	345	3	6	62,500	340 and 337.5/16.5	*HH*	650	48,660	616,000
					6	66,000					
0-26a	800,000	50	230	3				*CSC*	750	5,615	44,700
0-27a	800,000	50	345	3				*HH*	505	3,780	61,600
0-28a	800,000	75	230	3				*CSC*	800	8,970	67,000
0-29a	800,000	75	345	3				*HH*	505	5,668	92,400
0-30a	800,000	150	345	3				*HH*	505	11,336	184,900
0-31a	800,000	250	345	3	2	62,250	337.5/16.5	*HH*	505	18,890	308,000
					4	93,125					
0-32a	800,000	500	345	4	4	80,000	340 and 335/16.5	*HH*	900	89,700	821,400
					4	90,000					

For other major equipment items, see reference 8.

For generators, their design data, and means necessary for system stability, see Tables II and VI of reference 8. For sending-end and receiving-end transformers, see Tables II and VI of reference 8. For synchronous condensers at the receiving end of systems, see Table III of reference 8. For intermediate synchronous condensers, see Table V of reference 8. For conductors, see Table I of reference 8. For circuit layouts, see Figure 1 of reference 8. In both papers, the same system identification numbers are used.

of freight and delivery to site of erection is not included, one per cent is added to the cost of generating-plant and substation equipment, and three per cent is added to the cost of the transmission-line equipment proper. On top of the cost of equipment as delivered, certain percentages are added to cover costs of labor and installation on foundation. These percentages are determined from data obtained from the public utilities, manufacturers' service departments, and construction companies, and they vary with the type of equipment and voltage.

To the cost of equipment as installed on foundation, two more items are added: The indirect expenses constitute one item. They include engineering expenses, field supervision, miscellaneous construction expenses, legal expenses, expenses contingent on engineering and financial methods (such as taxes and insurance during construction), and normal contingencies (such as injuries and damages). To cover this item, 22.5 per cent is added. The other item is the cost of money during construction. This is taken at six per cent for one-half the length of the construction period and is calculated on the entire cost, including the indirect expenses. The construction period varies from six months for the smaller systems to 18 months for the larger.

A typical calculation of system capital cost is given in the appendix.

Salient Points on the Cost of Principal System Parts

All prices are based on a cost of copper of $10^{1}/_{8}$ cents per pound of ingot copper, free on board New York, and on a cost of fabricated steel of shapes involved in structures of $102 per ton, free on board Pittsburgh.

Standard prices are taken for waterwheel generators of normal design, with appropriate price increases for special design requirements. In the few cases where the cost of water turbines had to be accounted for, these are of the standard Francis type with a uniform water head of 75 feet. The prices of power transformers, high-voltage oil circuit breakers, air-break disconnectors, and lightning arresters for voltages up to 345 kv are given in the appendix.

Costs of copper conductors, Copperweld ground wires, and the counterpoise were calculated by the manufacturers from the ingot-copper price taken in the study. Wherever the *HH*-type hollow conductors are used, their cost is taken at a value five per cent higher than that of concentric stranded copper conductors of equal cross section. The prices of all hollow cables are sometimes lower and at other times higher than the prices of the concentric stranded copper cables. The somewhat higher price for the hollow conductors, however, has been taken in order to forestall possible objections to taking both types of conductors at equal prices. Had aluminum conductors been chosen instead, the over-all economic results would remain substantially the same.

The costs of a few subdivisions of the

Some of Their Major Equipment Items

Items, See Note Below

Ground Wire		Counterpoise 5/16-Inch Rods (Thousand Pounds)	High-Voltage Oil Circuit Breakers	High-Voltage Air-Break Disconnectors		High-Voltage Lightning Arresters (Total Number of Three-Phase Units)	Remarks	System Identification Number
Diameter (Inch)	Total Weight (Thousand Pounds)		Number of Three-Phase Oil Circuit Breakers in System and Their Data**	Total Number in System	Amperes			
3/8	180	80	2(8)1,000; 6(8)500	16	600	4		0-1a
3/8	180	80	8(8)750	16	600	4		0-2a
3/8	267	119	2(3)1,000; 2(3)500; 4(8)500	16	600	4		0-3a
3/8	267	119	8(8)750	16	600	4		0-4a
3/8	527	239	12(8)750	24	600	4		0-5a
3/8	877	398	12(8)750	27	600	4	One *IC* at mid-point***	0-6a
3/8	180	80	2(8)1,500; 8(8)750	20	600	4		0-7a
3/8	267	119	2(8)1,500; 8(8)750	20	600	4		0-8a
3/8	527	239	8(4)1,500; 6(8)750	28	600	4		0-9a
1/2	1,686	477.5	14(8)2,500	28	1,200	4		0-10a
1/2	2,806	796	14(8)2,500	31	1,200	4	One *IC* at mid-point	0-11a
3/8	180	80	8(3)2,500; 6(8)2,500; 10 (8)1,500	24 24	600 1,200	4		0-12a
1/2	576.5	159	24(8)2,500	48	1,200	4		0-13a
1/2	854	239	8(6)2,500; 16(8)2,500	48	1,200	4		0-14a
1/2	1,686	477.5	24(8)2,500	48	1,200	4		0-15a
1/2	2,806	796	8(3)3,500; 16(8)2,500	50	1,200	4	One *IC* at mid-point	0-16a
1/2	2,806	796	8(3)3,500; 16(8)2,500	50	1,200	4	One *IC* at mid-point	0-17a
1/2	5,591	1,592	24(8)2,500	52	1,200	4	Two *IC* equally spaced	0-18a
1/2	865	239	42(8)3,500; 8(8)2,500	100	1,200	6		0-19a
1/2	1,281	358	12(6)3,500; 26(8)3,500; 12(8)2,500	100	1,200	6		0-20a
1/2	2,529	715	16(6)3,500; 30(8)3,500; 8(8)2,500	108	1,200	6		0-22a
1/2	1,686	477.5	26(8)3,500; 22(8)2,500	96	1,200	4		0-23a
1/2	2,806	796	8(4)4,000; 28(8)4,000; 2(8)3,000	98	1,200	4	One *IC* at mid-point	0-24a
1/2	8,386	2,387	58(8)3,000	124	1,200	6	Two *IC* equally spaced	0-25a
1/2	865	239	12(4)6,000; 26(8)6,000; 8(8)4,500; 4(8)3,500	100	1,200	6		0-26a
1/2	865	239	38(8)6,000; 8(8)4,500; 4(8)3,500	100	1,200	6		0-27a
1/2	1,281	358	38(8)5,500; 8(8)4,500; 4(8)3,500	100	1,200	6		0-28a
1/2	1,281	358	12(6)5,500; 26(8)5,500; 8(8)4,500; 4(8)3,000	100	1,200	6		0-29a
1/2	2,529	715	42(8)5,500; 8(8)4,000; 4(8)3,000	108	1,200	6		0-30a
1/2	4,209	1,194	16(6)5,500; 26(8)5,500; 8(8)4,000; 4(8)3,000	112	1,200	6	One *IC* at mid-point	0-31a
1/2	11,181	3,182	32(6)5,000; 24(8)5,000; 8(8)4,000; 4(8)3,000	148	1,200	8	Two *IC* equally spaced	0-32a

* *CSC* denotes concentric stranded copper conductor. *HH* denotes hollow-type copper conductor, General Cable Corporation patents.

** First figure indicates number of three-phase oil circuit breakers, second figure (in parentheses) indicates breaker opening time in cycles, third figure indicates breaker interrupting capacity in megavolt-amperes.

*** *IC* denotes intermediate synchronous condenser placed for 100 per cent load.

six major groups forming the system capital cost structure are given in Table II. These costs are determined from the study of data obtained from public utilities and construction companies and from the literature.[9–12] The data are for items installed or on foundation in various parts of the country. These data were reduced to the price level of the 12-month period preceding the war and analyzed and averaged.

Since the supporting structures are one of the major system cost items, the details on the type and number of structures and foundations, as well as their estimated weights, are given in Table III. It is assumed that the steel-construction company will build and deliver the structures, install foundations (supplying the required cement), erect the structures, and string all conductors and ground wires. As quoted by steel-construction companies, this adds to the price of $102 per ton of fabricated steel $70 per ton of structure weight, when erected on grillage anchor, and $95 for structures erected on concrete anchors. The indirect expenses on the supporting structures are not included twice—by the steel-construction company and in this study—a corresponding adjustment having been made in the final structure cost. A slight reduction in cost is also made for structures erected on a common right of way.

The Right of Way

The cost of the right of way is frequently one of the most important items of the system capital cost. Still in numerous studies it is entirely neglected. A study of rights of way in various parts of the country shows that the line may pass through all types of territory: urban, suburban, and rural. It is very seldom that the line passes through one type of territory at a uniform cost per mile of the right of way. In the present study it is assumed that all lines pass through urban and rural territory, and that all right of way is held in fee. It is assumed that the length of the urban right of way in each system is a function of the length of the transmission line and the block of power transmitted, varying from a total of four miles in the shorter and lighter systems to 18 miles in the longest and heaviest lines. The total length of the urban right of way need not necessarily be concentrated at one or both ends of the line but may be scattered in various parts of it. Under the assumption made, the percentage which the length of the urban right of way constitutes of the total length of the line decreases as the latter increases.

In the existing transmission lines all over the country the width of the right of way held in fee varies in comparable lines more than in 1-to-5 ratio. In determining the width of the urban and of the rural right of way in the systems under study, the average height of the towers and the width of the longest crossarm are taken as guiding factors. Thus in one-tower-line systems the width varies from 100 feet in 69-kv systems to 175 feet in 345-kv systems. Where two tower lines are on a common right of way, the tower center-to-center distance is taken at not less than the average height of towers. In such systems the width of the right of way varies from 225 feet in some 230-kv systems to 290 feet in some 345-kv sys-

Table II. Cost of Various System Parts as Taken in the Study (Subdivisions)

	Cost of Land for Substation and Switch-House Sites				Cost of Structures for Substations Including Cost of Switch House		Cost of High-Voltage Busses and Supports in Substation (Installed)		Cost of Low-Voltage Switching, Protective, and Control Equipment of Substation (Installed)			Cost of Auxiliary Substation Equipment (Installed)			Cost of Clearing Brush and Trimming Trees on the Right of Way	
	Sending and Receiving Substations (Each)		Intermediate-Condenser and Sectionalizing Substations (All)													
System Identification Number	Total Land Area (Square Feet)	Total Cost (Dollars)	Total Land Area (Square Feet)	Total Cost (Dollars)	Sending and Receiving Substations (Each) Cost in Thousands of Dollars	Intermediate-Condenser and Sectionalizing Substations (All) Cost in Thousands of Dollars	Sending and Receiving Substations (Each) Cost in Thousands of Dollars	Intermediate-Condenser and Sectionalizing Substations (All) Cost in Thousands of Dollars	Sending Substation Cost in Thousands of Dollars	Intermediate-Condenser and Sectionalizing Substations (All) Cost in Thousands of Dollars	Receiving Substation Cost in Thousands of Dollars	Sending and Receiving Substations (Each) Cost in Thousands of Dollars	Intermediate-Condenser and Sectionalizing Substations (All) Cost in Thousands of Dollars	Cost of Plowing Counterpoise and Adjusting Tower-Footing Resistance in Thousands of Dollars	Number of Rural Acres to Be Cleared	Total Cost in Thousands of Dollars
0-1*a*	100,000	3,500			38		25		110.5		95	35		4	560	33.5
0-2*a*	100,000	3,500			52		29		126		109	39		3.5	700	42
0-3*a*	100,000	3,500			39.5		26		115		99	36		5.5	850	51
0-4*a*	100,000	3,500			53		29.5		128		110.5	39		5	1,070	64
0-5*a*	100,000	3,500	50,000	500	53	19	29.5	11	128	28	111	39	14	10	2,190	131.5
0-6*a*	100,000	3,500	50,000	500	53	37	30	21	129	53.5	112	40	28	17	3,680	221
0-7*a*	200,000	7,000			77		43		186		161	57		3.5	690	41.5
0-8*a*	200,000	7,000			78		43		188		163	58		5	1,060	63.5
0-9*a*	200,000	7,000	100,000	1,000	84	30	47	17	203	43.5	176	62.5	22.5	10	2,190	131.5
0-10*a*	200,000	7,000	100,000	1,000	131	44	58	19.5	255	51	218	80	27	19	4,350	261
0-11*a*	200,000	7,000	100,000	1,000	133	95	59	42	258.5	110	221.5	81	58	31.5	7,310	439
0-12*a*	500,000	17,500			214		119.5		517.5		448.5	159		3.5	680	41
0-13*a*	500,000	17,500			314.5		140		612		524	192.5		6.5	1,640	98
0-14*a*	500,000	17,500			320		142.5		623		534	196		9.5	2,530	152
0-15*a*	500,000	17,500			323		143.5		628		538	197.5		19	5,190	311
0-16*a*	500,000	17,500	250,000	2,500	349	216	155	96	679	252	582	214	132	31.5	7,260	435.5
0-17*a*	500,000	17,500	250,000	2,500	458	111.5	196	48	850	127	719	262	64	20	10,160	610
0-18*a*	500,000	17,500	500,000	5,000	434.5	428.5	186	183.5	807	490	683	248	245	58	20,600	1,236
0-19*a*	750,000	26,250			662		294.5		1,289		1,104	405		9.5	2,160	129.5
0-20*a*	750,000	26,250			670		298		1,305		1,119	409		14	3,350	201
0-22*a*	750,000	26,250	125,000	1,250	678	60	301.5	26.5	1,320	69.5	1,130	414	36	28.5	6,000	360
0-23*a*	750,000	26,250			832.5		357		1,549		1,308	476		17.5	6,000	360
0-24*a*	750,000	26,250	250,000	2,500	887	468	380	201	1,650	535	1,395	507	267.5	29	10,100	606
0-25*a*	750,000	26,250	500,000	5,000	864.5	709.5	370.5	304	1,607	810	1,359	494	406	87	26,400	1,584
0-26*a*	1,000,000	35,000			946		421		1,843		1,580	579		9.5	1,870	112
0-27*a*	1,000,000	35,000			975		418		1,814		1,534	557		8.5	2,480	149
0-28*a*	1,000,000	35,000			904		402		1,760		1,507	552		14	2,910	174.5
0-29*a*	1,000,000	35,000			1,061		455		1,972		1,668	606		13	3,860	232
0-30*a*	1,000,000	35,000	125,000	1,250	1,058	88	452	38	1,963	101	1,660	603	50.5	26	7,820	469
0-31*a*	1,000,000	35,000	250,000	2,500	1,065	417	456.5	179	1,980	477	1,674	609	238	43.5	13,340	800
0-32*a*	1,000,000	35,000	500,000	5,000	1,081	818	463.5	351	2,010	935	1,700	617	468	116	32,180	1,931

Table III. Supporting Structures and Right of Way

System Identification Number	Number of Tower Lines in System	Total Number of Towers in System	Type and Weight of Supporting Structure						Right of Way					
			Type A	Type B	Type C		Type D				Length		Cost Per Mile of a Single Right of Way of One or Two Widths, as Required	
			Steel Grillage Anchor, Weight in Tons	Steel Grillage Anchor, Weight in Tons	Steel Grillage Anchor, Weight in Tons	Concrete Anchor, Weight in Tons	Steel Grillage Anchor, Weight in Tons	Concrete Anchor, Weight in Tons	Note on Right of Way	Width (Feet)	Urban (Miles)	Rural (Miles)	Urban (Dollars)	Rural (Dollars)
0-1*a*	1	330	2.9	3.6	4.6	4.14	5.5	4.95		100	4.0	46.0	47,400	1,896
0-2*a*	1	293	3.5	4.5	5.5	4.95	6.5	5.85		125	4.0	46.0	50,000	2,000
0-3*a*	1	495	3.0	3.75	4.75	4.28	5.75	5.18		100	4.5	70.5	47,400	1,896
0-4*a*	1	440	3.5	4.5	5.5	4.95	6.5	5.85		125	4.5	70.5	50,000	2,000
0-5*a*	1	880	4.0	5.0	6.5	5.85	7.5	6.75		125	3.5	144.5	50,000	2,000
0-6*a*	1	1,466	4.5	5.5	7.0	6.30	8.5	7.65		125	7.0	243.0	50,000	2,000
0-7*a*	1	293	3.75	4.75	6.0	5.40	7.0	6.30		125	4.5	45.5	50,000	2,000
0-8*a*	1	440	4.0	5.0	6.5	5.85	7.5	6.75		125	5.0	70.0	50,000	2,000
0-9*a*	1	880	6.0	7.5	9.5	8.55	11.5	10.35		125	6.5	143.5	50,000	2,000
0-10*a*	2	1,584	4.0	5.0	6.5	5.85	7.5	6.75	Common right of way	250	6.5	143.5	69,500	2,700
0-11*a*	2	2,640	4.0	5.0	6.5	5.85	7.5	6.75	Common right of way	250	8.5	241.5	69,000	2,700
0-12*a*	1	293	8.5	10.5	13.5	12.15	16.0	14.40		125	5.0	45.0	50,000	2,000
0-13*a*	2	528	4.5	5.5	7.5	6.75	8.5	7.65	Two separate rights of way	150 and 150	5.0	45.0	52,600 and 52,600	2,104 and 2,104
0-14*a*	2	792	4.5	5.5	7.5	6.75	8.5	7.65	Two separate rights of way	150 and 150	5.5	69.5	52,600 and 52,600	2,104 and 2,104
0-15*a*	2	1,584	6.0	7.5	9.5	8.55	11.5	10.35	Two separate rights of way	150 and 150	7.5	142.5	52,600 and 52,600	2,104 and 2,104
0-16*a*	2	2,640	6.5	8.0	10.5	9.45	12.5	11.25	Two separate rights of way	125 and 125	10.0	240.0	50,000 and 50,000	2,000 and 2,000
0-17*a*	2	2,400	9.5	12.0	15.0	13.50	18.0	16.20	Two separate rights of way	175 and 175	10.0	240.0	55,200 and 55,200	2,208 and 2,208
0-18*a*	2	4,800	10.0	12.5	16.0	14.40	19.0	17.10	Two separate rights of way	175 and 175	14.0	486.0	55,200 and 55,200	2,208 and 2,208
0-19*a*	3	792	4.5	5.5	7.5	6.75	8.5	7.65	Two separate rights of way. One line on one right of way, two lines on another right of way	150 and 250	5.5	44.5	52,600 and 69,500	2,104 and 2,700
0-20*a*	3	1,188	4.5	5.5	7.5	6.75	8.5	7.65	Two separate rights of way. One line on one right of way, two lines on another right of way	150 and 250	6.0	69.0	52,600 and 69,500	2,104 and 2,700
0-22*a*	3	2,376	7.0	8.5	11.0	9.90	13.5	12.15	Two separate rights of way. One line on one right of way, two lines on another right of way	125 and 225	8.5	141.5	50,000 and 65,600	2,000 and 2,560
0-23*a*	2	1,440	9.5	12.0	15.0	13.50	18.0	16.20	Two separate rights of way	175 and 175	8.5	141.5	55,200 and 55,200	2,208 and 2,208
0-24*a*	2	2,400	9.5	12.0	15.0	13.50	18.0	16.20	Two separate rights of way	175 and 175	11.5	238.5	55,200 and 55,200	2,208 and 2,208
0-25*a*	3	7,200	11.0	13.5	17.5	15.75	21.0	18.90	Two separate rights of way. One line on one right of way, two lines on another right of way	175 and 275	16.0	484.0	55,200 and 73,400	2,208 and 2,840
0-26*a*	3	792	7.5	9.5	12.0	10.80	14.0	12.60	Two separate rights of way. One line on one right of way, two lines on another right of way	125 and 225	6.0	44.0	50,000 and 65,600	2,000 and 2,560
0-27*a*	3	720	9.5	12.0	15.0	13.50	18.0	16.20	Two separate rights of way. One line on one right of way, two lines on another right of way	175 and 290	6.0	44.0	55,200 and 75,700	2,208 and 2,925
0-28*a*	3	1,188	8.0	10.0	12.5	11.25	15.0	13.50	Two separate rights of way. One line on one right of way, two lines on another right of way	125 and 225	6.5	68.5	50,000 and 65,600	2,000 and 2,560
0-29*a*	3	1,080	9.5	12.0	15.0	13.50	18.0	16.20	Two separate rights of way. One line on one right of way, two lines on another right of way	175 and 290	6.5	68.5	55,200 and 75,700	2,208 and 2,925
0-30*a*	3	2,160	9.5	12.0	15.0	13.50	18.0	16.20	Two separate rights of way. One line on one right of way, two lines on another right of way	175 and 290	9.5	140.5	55,200 and 75,700	2,208 and 2,925
0-31*a*	3	3,600	9.5	12.0	15.0	13.50	18.0	16.20	Two separate rights of way. One line on one right of way, two lines on another right of way	175 and 290	13.0	237.0	55,200 and 75,700	2,208 and 2,925
0-32*a*	4	9,600	12.5	15.5	20.0	18.00	23.5	21.15	Two separate rights of way. Two lines on one right of way, two on another right of way	275 and 275	18.0	482.0	73,400 and 73,400	2,840 and 2,840

For 69- and 138-kv systems, supporting structures of conventional design used, with vertical configuration of conductors, two circuits per tower.

For 230- and 345-kv systems, the "rotated" type of tower, American Bridge Company patents, is used, with horizontal conductor configuration, one circuit per tower. Distance between adjacent conductors is 25 feet, 6 inches, for 230-kv structures, and 38 feet for 345-kv structures.

Average normal tower span is 800 feet for 69-kv structures, 900 feet for 138-kv, 1,000 feet for 230-kv, and 1,100 feet for the 345-kv structures.

Normal conductor sag calculations are made for heavy-loading-district conditions.

Type A: Suspension tower, with tangent towers, for angles up to 1 degree 30 seconds included.

Type B: Light-angle and long-span tower, for angles from 1 degree 30 seconds to 7 degrees, for extra-long spans without angle, and river-crossing towers.

Type C: Normal-strain tower, for angles from 7 degrees to 20 degrees. Railroad crossings with angles up to 5 degrees and transposition towers are in this class.

Type D: Extra-heavy-strain tower for angles from 20 degrees to 60 degrees. Dead-end towers and railroad crossings with angles 5 degrees to 30 degrees are in this class.

Percentages of each type of structure in any line are as follows: *A*—78 per cent, *B*—15 per cent, *C*—4 per cent, and *D*—3 per cent of the total number of towers in system.

One half of the type *C* and type *D* structures are on steel grillage anchor, and one half on concrete anchor foundation.

Where system has two separate rights of way of two different widths, the cost of both the urban and the rural right of way is given for each of the two widths and is calculated from a base cost of $50,000 for a mile of urban, and $2,000 for a mile of rural right of way of 125-foot standard width.

Table IV. Capital Cost of the Entire System and of Its Major Groups

System Identification Number	Total Capital Cost of Entire System in Thousands of Dollars	Capital Cost of Main System Groups in Per Cent of the Total System Capital Cost						Capital Cost of Entire System		Capital Cost of Transmission Line (Right of Way Not Included)			Capital Cost of Substations		
		Equipment Comparative Increment (Per Cent)	Sending Substation (Per Cent)	Transmission Line (Per Cent)	Right of Way (Per Cent)	All Intermediate Condenser and Sectionalizing Substations (Per Cent)	Receiving Substation (Per Cent)	Dollars Per Kilowatt Delivered	Dollars Per Mile	Dollars Per Kilowatt Delivered	Dollars Per Mile	Dollars Per Tower-Line-Mile	Sending Substation Dollars Per Kilowatt Delivered	All Intermediate-Condenser and Sectionalizing Substations Dollars Per Kilowatt Delivered	Receiving Substation Dollars Per Kilowatt Delivered
0-1*a*	2,776	6.9	18.2	22.6	10.1		42.2	55.50	55,500	12.60	12,550	12,550	10.10		23.40
0-2*a*	2,840		23.8	20.0	10.3		45.9	56.80	56,800	11.40	11,350	11,350	13.40		26.10
0-3*a*	3,418	5.8	15.4	31.3	10.3		37.2	68.40	45,600	21.40	14,250	14,250	10.50		25.40
0-4*a*	3,262		20.9	25.3	11.4		42.4	65.30	43,500	16.50	11,000	11,000	13.60		27.70
0-5*a*	4,800		14.3	42.7	12.0	4.5	26.5	96.00	32,000	41.10	13,700	13,700	13.70	4.30	25.40
0-6*a*	7,336		9.5	51.4	11.7	10.6	16.8	146.70	29,350	75.40	15,100	15,100	13.90	15.60	24.60
0-7*a*	4,151		24.1	15.9	7.8		52.2	41.50	83,000	6.60	13,150	13,150	10.00		21.70
0-8*a*	4,781		21.2	21.6	8.3		48.9	47.80	63,700	10.30	13,750	13,750	10.20		23.40
0-9*a*	7,486	2.1	14.7	38.2	8.4	4.8	31.8	74.90	49,900	28.60	19,100	19,100	11.00	3.50	23.80
0-10*a*	8,969		19.7	35.8	9.5	6.1	28.9	89.70	59,800	32.10	21,400	10,700	17.70	5.40	25.90
0-11*a*	13,412		13.5	39.8	9.5	17.3	19.9	134.10	53,650	53.30	21,300	10,650	18.10	23.30	26.70
0-12*a*	11,480	4.5	24.0	13.8	3.0		54.7	45.90	229,600	6.30	31,600	31,600	11.10		25.10
0-13*a*	13,272		31.6	9.3	5.5		53.6	53.10	265,400	4.90	24,700	12,350	16.80		28.50
0-14*a*	14,327		29.8	12.8	6.2		51.2	57.30	191,000	7.30	24,400	12,200	17.10		29.40
0-15*a*	18,668		24.0	23.5	7.6		44.9	74.70	124,450	17.60	29,250	14,625	18.00		33.50
0-16*a*	38,998	20.5	12.5	22.5	5.2	18.6	20.7	156.00	156,000	35.00	35,000	17,500	19.50	29.00	32.40
0-17*a*	33,420		21.4	28.9	6.7	12.2	30.8	133.70	133,700	38.70	38,700	19,350	28.50	16.30	41.20
0-18*a*	57,096	0.6	12.3	36.6	6.7	26.0	17.8	228.30	114,200	83.50	41,750	20,875	28.10	59.40	40.60
0-19*a*	26,061		33.8	6.8	3.5		55.9	52.10	521,200	3.60	35,700	11,900	17.60		29.20
0-20*a*	27,645		32.3	9.6	3.9		54.2	53.30	368,600	5.30	35,400	11,800	17.80		30.00
0-22*a*	38,010	3.1	24.5	21.5	4.4	1.9	44.6	76.00	253,400	16.30	54,500	18,165	18.60	1.50	33.90
0-23*a*	42,250	0.2	31.2	13.7	3.8		51.1	84.50	281,700	11.60	38,600	19,300	26.40		43.20
0-24*a*	63,249	1.5	22.4	15.3	3.8	23.4	33.6	126.50	253,000	19.40	38,700	19,350	28.30	29.70	42.50
0-25*a*	93,228	0.7	15.1	35.7	5.0	22.0	21.5	186.50	186,450	66.50	66,500	22,170	28.00	41.10	40.10
0-26*a*	38,465	1.3	32.8	7.5	2.4		56.0	48.10	769,300	3.60	57,700	19,230	15.70		27.00
0-27*a*	44,555		35.7	6.3	2.3		55.7	55.70	891,100	3.50	56,300	18,830	19.90		31.00
0-28*a*	45,134	10.8	27.3	10.0	2.4		49.5	56.40	601,800	5.70	60,400	20,130	15.40		28.00
0-29*a*	47,300		34.4	8.9	2.6		54.1	59.10	630,700	5.30	56,200	18,730	20.30		32.00
0-30*a*	55,430		30.1	15.2	3.6	2.3	48.8	69.30	369,500	10.60	56,250	18,750	20.80	1.60	33.80
0-31*a*	72,525	1.3	23.3	19.5	4.1	15.4	36.4	90.70	290,100	17.60	56,400	18,800	21.20	14.00	33.00
0-32*a*	122,231	0.8	14.2	42.6	4.6	17.1	20.7	152.80	244,500	65.00	104,000	26,000	21.80	26.20	31.60

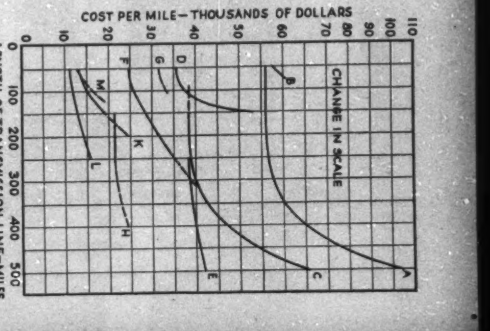


Figure 4. Capital cost of the transmission line per mile

Cost of the right of way is not included in the cost of the transmission line. For explanation of identification letters, see Figure 2

tems. Where there are two separate and remote rights of way with one tower line on one and two tower lines on another, these are of two different widths in accordance with the outlined principles.

The cost per mile of the right of way in the existing systems varies in a 1-to-12 ratio, reaching at times the figure of $100,000 per mile for a strip 125 feet wide. In the study made by the St. Lawrence Power Development Commission[13] the cost of the right of way held in fee was taken at $50,000 per mile for a strip 500 feet wide over the entire length of the 300-mile line. In the present study the cost per mile of a 125-foot strip is assumed at $50,000 for the urban and $2,000 for the rural right of way.

The study of the effect of the width of the right of way on its cost per mile shows that of all the items entering into its cost—fee paid to grantor, surveying, drafting, agent's expenses, recording deeds, title examination and other legal expenses, release of liens, and other minor costs—only the fee paid to grantor is in part proportional to the width of the right of way. This fee forms 70 to 78 per cent of the total cost. On the basis of the study made, it is assumed that when the width of the system right of way varies by 50 per cent or less from the standard width of 125 feet, 26 per cent of the total right-of-way cost—whether urban or rural—is proportional to its width. When, however, the width of the system right of way

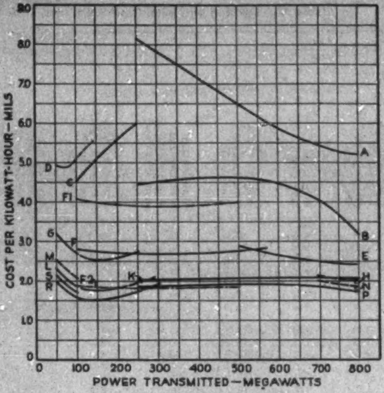


Figure 5. Cost of the entire system per kilowatt-hour

For explanation of identification letters, see Figure 1. All costs at 50 per cent annual load factor except as follows:

*F*1. 150–230 at 35 per cent
*F*2. 150–230 at 75 per cent annual load factor

varies from the standard width by more than 50 per cent, it is assumed that 39 per cent of the urban right-of-way cost and 35 per cent of the rural cost are proportional to its width. Under these assumptions the cost per mile of the urban right of way varies from $47,400 for a strip 100 feet wide to $75,700 for the 290-foot strip. In the case of the rural right of way the cost per mile varies correspondingly from $1,896 to $2,925.

The width and cost of the right of way in all systems are given in Table III.

System Capital Cost

Capital cost of the systems under study for the system as a whole, for the transmission line, and for the substations is given in Table IV. Costs are calculated per kilowatt transmitted, per mile, and per tower-line-mile. Cost of the six major groups into which the capital cost structure of the system is divided is given also in per cent of the total system cost. Considering all factors involved, the costs obtained compare favorably with the costs of existing systems.

The capital costs per kilowatt transmitted and per mile for the entire system and for the transmission line alone are represented in Figures 1, 2, 3, and 4.

It is seen from the figures that there is an economic relationship between the magnitude of the block of power transmitted, the transmission distance, and the voltage.

When power is transmitted over distances up to about 200 miles at voltages up to and including 230 kv, the most economical block of power is not the largest that may be transmitted over the system under accepted conditions of reliability. Transmission of a block considerably smaller than the largest will result in a more economical solution. For example, transmission of about 150,000 kw over 50 miles at 138 kv costs approximately six dollars per kilowatt less than if the system were to transmit 250,000 kw, and about 11 dollars less, were it transmitting 300,000 kw. When large blocks are to be transmitted, it is more economical to pass to higher voltages. For example, curves *G*, *F*, and *E* in Figures 1 and 3 show that when the transmission distance is 150 miles, a transmission voltage of 138 kv should be used with blocks of power not exceeding about 225,000 kw; after which a 230-kv voltage is more economical until the transmission of approximately 550,000 kw is reached. As kilowatts are further increased, a 345-kv voltage is the most economical until this block of power is about doubled. That the transmission voltage should be raised when a certain block of power is transmitted over gradually increasing distances, is a well-known fact. Curves *D*, *C* and *F*, *E* of Figures 2 and 4, however, show at what mileages the increase in voltage should take place for economical transmission. At transmission distances such as 500 miles, power cannot be transmitted economically at a voltage below 345 kv because of stability considerations. At this distance very small blocks of power may be transmitted at 230 kv, but at an entirely prohibitive cost.

As the transmission distance increases, the cost of the transmission line per mile increases, because of the effect the voltage required has on the cost of the conductors and supporting structures. Simultaneously, the cost of the entire system per mile decreases, because of better utilization of copper in the line resulting from the effect the higher voltage has on system stability. For the same reason the cost of the transmission line per kilowatt transmitted goes down as the block of power transmitted increases. The rate at which this cost decreases in the short transmission lines is much more rapid than in the longer lines. This shows that, insofar as the cost of the line per kilowatt is concerned, once the voltage is properly chosen, the transmission of large blocks of power over long distances is not as critical as the transmission of small blocks over short distances. On the other hand, had the block of power been increased to about 1,200,000 kw, the cost per kilowatt transmitted over a distance of 800 miles would again tend to rise, indicating that a transmission voltage of 460 kv is in order, if lower costs are to be obtained.

In every case of transmission, therefore, where either the distance of transmission or the magnitude of the block of power is fixed, there is a most economical relationship between the block of power, the length of the line, and the transmission voltage.

Table IV shows that the capital cost of

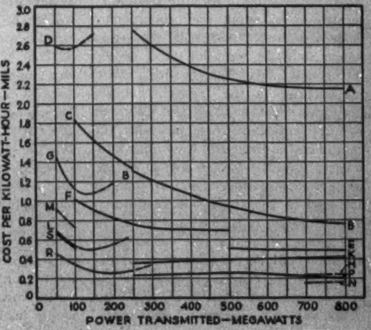


Figure 6. Cost of the transmission line per kilowatt-hour at 50 per cent annual load factor

Cost of the right of way is not included in the cost of the transmission line. For explanation of identification letters, see Figure 1

Table V. Division of Capital Cost of the Transmission

System Identification Number	Capital Cost of the Component Parts of the Transmission Line in Per Cent of the Capital Cost of the Line							Capital Cost of the Component				
								Sending Substation				
	Clearing Brush and Trimming Trees (Per Cent)	Supporting Structures (Per Cent)	Overhead Conductors (Per Cent)	Ground Wires (Per Cent)	Insulators and Hardware (Per Cent)	Counterpoise and Tower-Footing Resistance (Per Cent)	Equipment for Communication and Relaying (Per Cent)	Structures (Per Cent)	Power Transformers (Per Cent)	High-Voltage Switching and Protective Equipment (Per Cent)	Low-Voltage Switching, Protective and Control Equipment (Per Cent)	Auxiliary Substation Equipment (Per Cent)
0-1*a*	6.6	32.2	39.4	6.3	3.7	2.7	9.1	9.3	35.0	19.1	27.1	8.5
0-2*a*	9.1	38.4	24.8	7.0	7.2	2.9	10.6	9.6	34.1	25.0	23.4	7.2
0-3*a*	6.0	29.0	48.0	5.6	3.4	2.4	5.6	9.3	35.0	19.0	27.2	8.5
0-4*a*	9.6	39.6	25.6	7.1	7.5	2.9	7.7	9.6	34.3	24.3	23.4	7.2
0-5*a*	8.0	36.6	36.4	5.7	5.8	2.4	5.1	9.7	34.3	24.7	23.4	7.2
0-6*a*	7.4	37.5	39.3	5.2	5.3	2.2	3.1	9.7	34.5	24.6	23.4	7.2
0-7*a*	7.8	35.5	32.7	6.0	6.2	2.4	9.4	9.5	36.5	23.0	23.1	7.1
0-8*a*	7.8	36.2	36.0	5.7	5.7	2.3	6.3	9.5	36.7	22.8	23.1	7.1
0-9*a*	5.7	39.4	41.1	4.1	4.2	1.7	3.8	9.5	34.4	25.0	23.1	7.1
0-10*a*	10.2	40.0	25.9	11.6	5.7	3.0	3.6	9.3	35.6	30.9	18.1	5.7
0-11*a*	10.4	40.5	26.2	11.7	5.7	3.0	2.5	9.3	35.9	30.5	18.1	5.7
0-12*a*	3.2	33.3	53.2	2.5	2.6	1.0	4.2	9.6	34.1	25.2	23.2	7.1
0-13*a*	9.9	40.8	26.3	10.2	4.9	2.5	5.4	9.3	35.8	30.4	18.2	5.7
0-14*a*	10.2	41.3	26.6	10.2	5.0	2.6	4.1	9.3	35.7	30.6	18.2	5.7
0-15*a*	8.9	46.2	28.1	8.5	4.2	2.2	1.9	9.0	35.3	28.7	17.5	5.5
0-16*a*	6.3	42.1	38.0	7.1	3.4	1.8	1.3	9.0	32.1	32.1	17.6	5.5
0-17*a*	7.9	50.8	27.6	6.4	4.3	1.6	1.4	8.1	31.9	40.0	15.1	4.6
0-18*a*	7.6	50.2	29.7	6.0	4.1	1.5	0.9	7.9	33.6	36.7	14.7	4.5
0-19*a*	9.0	40.9	27.1	10.6	5.1	2.7	4.6	9.4	31.2	35.2	18.2	5.7
0-20*a*	9.4	41.3	27.5	10.6	5.1	2.6	3.5	9.3	31.0	35.4	18.2	5.7
0-22*a*	5.5	41.8	39.1	6.8	3.2	1.8	1.8	9.1	30.5	34.1	17.8	5.6
0-23*a*	7.8	50.5	27.4	6.5	4.4	1.6	1.8	7.9	32.1	38.8	14.7	4.5
0-24*a*	7.9	50.7	27.5	6.4	4.3	1.6	1.6	7.9	30.6	40.3	14.7	4.5
0-25*a*	6.1	50.1	32.0	5.7	3.8	1.4	0.9	7.9	31.0	40.0	14.7	4.5
0-26*a*	4.8	42.2	38.5	6.6	3.1	1.7	3.1	9.3	27.9	38.4	18.2	5.7
0-27*a*	6.5	49.6	27.9	6.7	4.4	1.6	3.3	7.6	33.4	40.2	14.2	4.4
0-28*a*	4.8	42.9	39.3	6.2	2.9	1.6	2.3	9.1	29.2	35.4	17.8	5.6
0-29*a*	6.8	49.9	27.9	6.7	4.4	1.7	2.6	8.1	32.9	39.1	15.1	4.6
0-30*a*	6.9	50.3	28.1	6.6	4.5	1.7	1.9	7.9	32.7	37.7	14.8	4.5
0-31*a*	7.1	50.5	28.3	6.6	4.5	1.7	1.3	7.9	32.5	37.9	14.8	4.5
0-32*a*	4.8	48.6	36.5	4.8	3.3	1.2	0.8	7.9	32.3	38.1	14.8	4.5

The cost of land for the substations has been omitted from the table. The percentage cost of this remaining item may be determined from the given percentages of the component parts of substations.

the receiving-end substations is greater than that of the sending-end substations. The reason for this is that the former include synchronous condensers, and their power transformers are more expensive.

The total capital cost of some systems is greatly influenced by the cost of various measures applied for the increase of the system transient-stability limit. That this expenditure is highly justified will now be shown in a few extreme cases.

In the system 0-1*a* the cost of stability measures,[8] including the proportionate share of indirect expenses and the cost of money, is $1.20 per kilowatt of system rating, or a total of $60,000, with a resulting increase in the transient-stability limit from 44,600 to 50,000 kw. Without the application of these measures the capital cost of the entire system per kilowatt transmitted (44,600 kw) is $60.90, while the expenditure of $60,000 lowered the cost to $55.50 per kilowatt transmitted (50,000 kw), or 8.9 per cent. In the system 0-6*a* the cost of stability measures is $12.50 per kilowatt of system rating, with a resulting increase in the transient-stability limit from 42,250 to 50,000 kw. The cost per kilowatt transmitted is reduced from $159 to $146.70, or 7.7 per cent. In the system 0-15*a* the cost of all stability measures is $6.10 per kilowatt of system rating. This increases the transient-stability limit from 157,500 to 250,000 kw, with a cost reduction from $108.80 to $74.70 per kilowatt transmitted, or 31.3 per cent. In the system 0-18*a* the cost of the stability measures exceeds that of any other system and amounts to $72.15 per kilowatt of system rating. This results in an increase of system transient-stability limit from 136,300 to 250,000 kw and lowers the cost per kilowatt transmitted from $287 to $228.30, or 20.4 per cent. In the case of the longest and heaviest system, 0-32*a*, the cost of all stability measures is $36.40 per kilowatt of system rating, or a total of $29,120,000. As a result of this expenditure the system transient-stability limit is raised from 398,200 to 800,000 kw, and the cost per kilowatt transmitted is lowered from $234 to $152.80, or 34.8 per cent.

In every case the application of various devices increasing the transient-stability limit results in substantial decrease of the capital cost per kilowatt transmitted, thus amply justifying their use. In the same proportion the economy obtained is reflected in the system annual cost. The economy is, of course, predicated on the premise that the additional power it is possible to transmit can find a ready market.

The analysis shows that the least expensive of all the means applied to increase the transient-stability limit is the use of high-speed circuit breakers. In the system 0-3*a*, for example, where nothing but three-cycle-opening-time circuit breakers are used, these cost only 14 cents per kilowatt of system rating. Their application increases the transient-stability limit from 37,000 to 50,000 kw, decreasing the cost per kilowatt transmitted by 25.7 per cent, or from $92.00 to $68.40.

The average cost of the intermediate-synchronous-condenser stations in the economical systems is $17.80 per kilowatt transmitted. This cost includes the synchronous condensers and their transformers, the required high-voltage and low-voltage equipment—installed on foundation—the proportionate cost of substation structures, the corresponding indirect expenses, and the cost of money. Had series capacitors been used instead, the cost of the intermediate-series-capaci-

Line and Substations Into Their Component Parts

Parts of Substations in Per Cent of the Capital Cost of the Substation

All Intermediate-Condenser and Sectionalizing Substations						Receiving Substation						
Structures (Per Cent)	Power Transformers (Per Cent)	Synchronous Condensers (Per Cent)	High-Voltage Switching and Protective Equipment (Per Cent)	Low-Voltage Switching Protective and Control Equipment (Per Cent)	Auxiliary Substation Equipment (Per Cent)	Structures (Per Cent)	Power Transformers (Per Cent)	Synchronous Condensers (Per Cent)	High-Voltage Switching and Protective Equipment (Per Cent)	Low-Voltage Switching (Protective) and Control Equipment (Per Cent)	Auxiliary Substation Equipment (Per Cent)	System Identification Number
						4.0	14.8	58.6	8.4	10.1	3.7	0-1a
						5.0	25.6	42.0	12.8	10.4	3.7	0-2a
						3.9	14.4	59.9	8.2	9.6	3.6	0-3a
						4.7	24.6	44.9	12.1	9.9	3.5	0-4a
11.3	—	—	63.6	16.3	8.4	5.2	26.5	39.8	13.3	10.9	3.9	0-5a
6.0	14.7	45.4	20.6	8.7	4.5	5.5	27.8	36.9	13.9	11.5	4.1	0-6a
						4.4	25.5	46.3	11.0	9.2	3.3	0-7a
						4.1	23.9	49.7	10.2	8.7	3.1	0-8a
10.7	—	—	65.5	15.4	7.9	4.4	23.6	47.7	11.6	9.2	3.3	0-9a
10.1	—	—	71.7	11.8	6.2	6.3	32.4	25.5	21.1	10.5	3.9	0-10a
5.1	17.2	49.3	19.2	6.0	3.1	6.3	31.7	26.8	20.7	10.5	3.8	0-11a
						4.2	23.5	48.1	11.6	8.9	3.2	0-12a
						5.5	29.3	34.4	17.9	9.1	3.4	0-13a
						5.4	28.6	35.5	17.8	9.0	3.3	0-14a
						4.8	25.8	42.6	15.4	8.0	3.0	0-15a
3.7	24.6	53.0	1.9	4.4	2.3	5.4	26.1	36.4	19.3	9.1	3.3	0-16a
3.8	30.5	56.7	2.7	4.2	2.1	5.6	33.0	21.5	27.7	8.8	3.2	0-17a
3.7	30.5	57.3	2.2	4.2	2.1	5.5	34.0	23.1	25.4	8.6	3.1	0-18a
						5.6	25.2	34.2	21.9	9.4	3.5	0-19a
						5.6	24.7	36.0	21.0	9.3	3.4	0-20a
10.1	—	—	71.7	11.8	6.2	5.0	22.8	41.2	19.3	8.3	3.1	0-22a
						4.8	29.6	30.2	25.0	7.6	2.8	0-23a
4.0	33.5	53.7	2.0	4.5	2.3	5.3	30.3	26.3	25.0	8.3	3.5	0-24a
4.4	28.9	48.5	10.6	5.0	2.5	5.5	32.1	22.5	27.9	8.7	3.2	0-25a
						5.4	21.5	38.1	22.4	9.1	3.3	0-26a
						4.9	30.9	28.0	25.7	7.7	2.8	0-27a
						5.0	20.8	43.1	19.5	8.4	3.1	0-28a
						5.1	30.0	28.9	24.8	8.1	3.0	0-29a
8.6	—	—	76.4	9.9	4.9	4.9	28.7	32.6	23.3	7.7	2.8	0-30a
4.7	29.8	45.7	11.7	5.4	2.7	5.1	29.7	29.9	24.3	8.0	2.9	0-31a
5.0	22.1	42.2	22.1	5.7	2.9	5.5	31.5	24.9	26.3	8.6	3.1	0-32a

tor stations would undoubtedly be lower. In two-circuit lines, however, the intermediate-capacitor station, like the intermediate-synchronous-condenser station, would contain equipment for transmission-line sectionalizing. Therefore the over-all economy obtained in the total system cost may not be as significant perhaps as might appear at first when the use of series capacitors is planned.

Division of System Capital Cost Into Its Component Parts

The percentage division of the system capital cost into its six main component parts is given in Table IV. The table shows that the receiving-end substation is the largest cost item in the total cost. The sending-end substation and the transmission line are next in magnitude of cost. The percentage costs of the three main items vary greatly, depending on the relationship between the length of the line and the block of power transmitted. The total cost of all the intermediate-condenser and sectionalizing substations also constitutes a large item in some systems. The percentage cost of the right of way is a smaller item and varies within narrower limits.

Table VI. Annual Fixed Charges and Annual Expenses for Maintenance and Operation on System Parts, in Per Cent of Their Capital Cost

Item	Annual Fixed Charges					Annual Expenses for Maintenance and Operation (Per Cent)
	Taxes (Per Cent)	Insurance (Per Cent)	Depreciation and Obsolescence (Per Cent)	Interest on Money Invested (Per Cent)	Total (Per Cent)	
Equipment comparative increment cost	2.0	0.5	1.823	6.0	10.323	2.0
Sending substation	2.0	0.5	1.823	6.0	10.323	2.0
Transmission line	2.0	—	1.823	6.0	9.823	1.3
Right of way	2.0	—	—	6.0	8.0	0.25
Intermediate-condenser and sectionalizing substations	2.0	0.5	1.823	6.0	10.323	2.0
Receiving substation	2.0	0.5	1.823	6.0	10.323	2.0

Charges for depreciation and obsolescence calculated by the sinking-fund method, at six per cent per annum money cost, for a 25-year life span of all system parts.

The percentage division of the capital costs of the transmission line and of the substations into their component parts is given in Table V. The costs of the supporting structures and of the overhead conductors form the major component parts of the line. The percentage variation of these items depends on the length (or the voltage) and on the load of the line. The cost of the counterpoise and of adjusting the tower-footing resistance is the smallest of all the items.

The costs of power transformers and high-voltage switching and protective equipment constitute the major items of the sending-substation cost. Synchronous condensers and power transformers occupy the first place in the subdivisions of the cost of the receiving-end substation. The cost of land, held in fee, is the smallest of all items; it rarely exceeds two per cent of the substation cost and frequently is less than this figure.

The percentage division of the system capital cost into its component parts makes it possible to evaluate the effect of changes in the cost of system parts or basic materials. If the cost of any sys-

Table VII. Total

			Annual Cost of Losses for Entire System								
			Total Annual Kilowatt-Hours of Losses in System			Cost of Energy Loss Per Kilowatt-Hour					
						At 35 Per Cent Annual Load Factor*		At 50 Per Cent Annual Load Factor*		At 75 Per Cent Annual Load Factor*	
System Identification Number	Annual Fixed Charges for Entire System (Thousands of Dollars)	Annual Expenses for Maintenance and Operation for Entire System (Thousands of Dollars)	At 35 Per Cent Annual Load Factor (Millions of Kwhrs)	At 50 Per Cent Annual Load Factor (Millions of Kwhrs)	At 75 Per Cent Annual Load Factor (Millions of Kwhrs)	Cost of Transmission (Mils Per Kwhr)	Cost of Energy Loss (Mils Per Kwhr)	Cost of Transmission (Mils Per Kwhr)	Cost of Energy Loss (Mils Per Kwhr)	Cost of Transmission (Mils Per Kwhr)	Cost of Energy Loss (Mils Per Kwhr)
0-1*a*	277	46	16.0	21.8	34.4	2.11	9.68	1.47	6.84	0.98	4.62
0-2*a*	283	48	11.2	14.4	21.5	2.16	9.73	1.51	6.88	1.01	4.65
0-3*a*	340	55	16.8	22.8	35.7	2.57	10.14	1.80	7.17	1.20	4.84
0-4*a*	324	53	13.7	18.1	27.7	2.46	10.03	1.72	7.09	1.15	4.79
0-5*a*	472	71	16.0	20.4	30.0	3.55	11.12	2.48	7.85	1.65	5.29
0-6*a*	719	104	22.0	28.1	41.1	5.37	12.94	3.76	9.13	2.51	6.15
0-7*a*	417	73	22.8	30.3	46.7	1.60	9.17	1.12	6.49	0.75	4.39
0-8*a*	479	81	28.5	38.1	59.1	1.83	9.40	1.28	6.65	0.86	4.50
0-9*a*	744	119	31.7	43.2	68.3	2.82	10.39	1.97	7.34	1.32	4.96
0-10*a*	890	142	19.8	25.9	39.2	3.36	10.93	2.35	7.72	1.57	5.21
0-11*a*	1,328	209	39.0	50.8	76.5	5.01	12.58	3.50	8.87	2.34	5.98
0-12*a*	1,169	213	58.5	74.9	110.9	1.81	9.38	1.26	6.63	0.84	4.48
0-13*a*	1,347	244	48.8	62.5	92.4	2.08	9.65	1.45	6.82	0.97	4.61
0-14*a*	1,449	258	55.6	73.0	111.0	2.23	9.80	1.56	6.93	1.04	4.68
0-15*a*	1,872	318	77.0	103.8	162.4	2.86	10.43	2.00	7.37	1.34	4.98
0-16*a*	3,936	683	169.6	204.8	281.7	6.03	13.60	4.22	9.59	2.81	6.45
0-17*a*	3,348	562	83.1	108.3	163.5	5.10	12.67	3.57	8.94	2.38	6.02
0-18*a*	5,702	928	159.7	198.7	284.2	8.65	16.22	6.05	11.42	4.04	7.68
0-19*a*	2,661	493	108.7	143.3	218.9	2.06	9.63	1.51	6.88	0.96	4.60
0-20*a*	2,816	515	128.3	174.6	275.2	2.17	9.74	1.52	6.89	1.02	4.66
0-22*a*	3,845	674	154.6	204.1	312.1	2.95	10.52	2.06	7.43	1.38	5.02
0-23*a*	4,295	776	127.6	168.2	257.1	3.31	10.88	2.32	7.69	1.55	5.19
0-24*a*	6,425	1,155	223.7	291.5	439.1	4.95	12.52	3.46	8.83	2.31	5.95
0-25*a*	9,349	1,549	250.3	320.9	475.2	7.10	14.67	4.97	10.34	3.32	6.96
0-26*a*	3,936	733	162.6	208.6	308.8	1.91	9.48	1.33	6.70	0.89	4.53
0-27*a*	4,562	853	134.8	168.8	243.0	2.21	9.78	1.54	6.91	1.03	4.67
0-28*a*	4,612	852	201.5	262.6	396.0	2.23	9.80	1.56	6.93	1.04	4.68
0-29*a*	4,834	895	148.7	190.2	280.6	2.34	9.91	1.63	7.00	1.09	4.73
0-30*a*	5,634	1,014	192.1	256.9	398.6	2.71	10.28	1.90	7.27	1.27	4.91
0-31*a*	7,346	1,299	258.1	340.8	522.1	3.53	11.10	2.47	7.84	1.65	5.29
0-32*a*	12,227	1,982	326.6	433.8	664.8	5.80	13.37	4.05	9.42	2.70	6.34

* Cost of hydrogeneration is taken at 7.57 mils per kilowatt-hour at 35 per cent annual load factor, 5.37 mils per kilowatt-hour at 50 per cent annual load factor, and 3.64 mils per kilowatt-hour at 75 per cent annual load factor.

tem part is changed, it is only necessary to find from Tables IV and V the percentage the cost of the particular part forms of the total system cost, calculate in per cent the change that is introduced into the cost, and multiply it by the figure found from the tables. The figure thus obtained will show the resulting percentage change in the capital cost per kilowatt. When it is desired to estimate the effect of changes in prices of copper or steel, use must be made of quantities of these materials in various system parts, such as overhead conductors, supporting structures, and so forth, given in Tables I and III. The cost of a particular system part is calculated at the new basic price of copper or steel, and the procedure described may then be applied.

As an example, the cost of clearing brush will be considered. From Tables IV and V it may be found that for a particular system the cost of clearing brush and trimming trees represents about 3.5 per cent of the total capital cost. Should the cost per acre of clearing brush double, it will result in about 3.5 per cent increase in the capital cost of the system per kilowatt.

In the case of the right of way, for example, if in a particular system its width, the proportion of urban and rural right of way, and its cost per mile should change, the effect of the width on the cost per mile and the total cost of the right of way in the system may be calculated from data given in Table III. From Tables IV and V the total cost of the original right of way in dollars at the old price may be calculated, as well as the percentage this cost forms of the total capital cost. The ratio of the dollar costs of the new and of the original right of way is then determined and applied to the percentage which the cost of the original right of way forms of the total system capital cost. The resulting percentage increase in the capital cost per kilowatt may now be easily estimated.

One of the most important items affecting the capital cost are the indirect expenses. It will be recalled that these are taken care of by adding 22.5 per cent to the cost of all system items, except the right of way. Assume, for example, that in a system where the cost of the right of way constitutes ten per cent of the total capital cost, an estimate must be made of the effect on the cost per kilowatt of indirect expenses amounting to 27.5 per cent. From these figures it may be calculated that this increase in the indirect expenses will raise the cost per kilowatt by about 3.6 per cent. The new cost per kilowatt may then be obtained by applying this percentage to the figure given in Table IV for the particular system.

In this fashion the percentage division of the capital cost may be made use of when it is desired to consider costs different from those employed in the paper.

Similarly when system design is changed and the amount of equipment used is modified, the new cost may be estimated by reference to both the equipment and the percentage-cost tables.

Method of Estimating System Annual Cost

The system annual cost is taken as the sum of the annual fixed charges and the annual operating expenses.

The annual fixed charges cover the following four items: taxes, insurance, depreciation and obsolescence, and interest on money invested. Every one of these items is calculated separately for each of the six major groups into which the capital cost structure is divided. The total annual fixed charges on various system parts are given in Table VI.

Total Annual Cost of Energy Loss: At 35 Per Cent Annual Load Factor (Thousands of Dollars)	Total Annual Cost of Energy Loss: At 50 Per Cent Annual Load Factor (Thousands of Dollars)	Total Annual Cost of Energy Loss: At 75 Per Cent Annual Load Factor (Thousands of Dollars)	Total Annual System Cost: At 35 Per Cent Annual Load Factor: Total Cost (Thousands of Dollars)	Total Annual System Cost: At 35 Per Cent Annual Load Factor: Cost Per Kwhr (Mils)	Total Annual System Cost: At 50 Per Cent Annual Load Factor: Total Cost (Thousands of Dollars)	Total Annual System Cost: At 50 Per Cent Annual Load Factor: Cost Per Kwhr (Mils)	Total Annual System Cost: At 75 Per Cent Annual Load Factor: Total Cost (Thousands of Dollars)	Total Annual System Cost: At 75 Per Cent Annual Load Factor: Cost Per Kwhr (Mils)	Cost of Kilowatt-Hour Energy at Receiving End: At 35 Per Cent Annual Load Factor (Mils)	Cost of Kilowatt-Hour Energy at Receiving End: At 50 Per Cent Annual Load Factor (Mils)	Cost of Kilowatt-Hour Energy at Receiving End: At 75 Per Cent Annual Load Factor (Mils)	System Identification Number
155	149	159	478	3.12	472	2.16	482	1.47	10.69	7.53	5.11	0-1*a*
109	99	100	440	2.87	430	1.97	431	1.31	10.44	7.34	4.95	0-2*a*
171	163	173	565	3.69	558	2.55	507	1.73	11.26	7.92	5.37	0-3*a*
138	129	133	515	3.35	506	2.31	510	1.55	10.92	7.68	5.19	0-4*a*
178	160	159	721	4.70	703	3.21	702	2.14	12.27	8.58	5.78	0-5*a*
286	256	254	1,109	7.23	1,080	4.93	1,077	3.27	14.80	10.30	6.91	0-6*a*
209	197	206	699	2.28	686	1.57	695	1.06	9.85	6.94	4.70	0-7*a*
268	254	266	829	2.71	815	1.86	827	1.26	10.28	7.23	4.90	0-8*a*
329	318	339	1,192	3.89	1,181	2.70	1,202	1.83	11.46	8.07	5.47	0-9*a*
216	200	205	1,248	4.07	1,232	2.81	1,237	1.88	11.64	8.18	5.52	0-10*a*
490	450	458	2,028	6.61	1,987	4.53	1,995	3.03	14.18	9.90	6.67	0-11*a*
549	497	497	1,931	2.52	1,879	1.72	1,879	1.15	10.09	7.09	4.79	0-12*a*
470	426	426	2,062	2.69	2,017	1.85	2,017	1.23	10.26	7.22	4.87	0-13*a*
544	506	520	2,252	2.94	2,214	2.02	2,228	1.36	10.51	7.39	5.00	0-14*a*
803	765	808	2,993	3.90	2,955	2.70	2,998	1.83	11.47	8.07	5.47	0-15*a*
2,305	1,964	1,820	6,924	9.02	6,583	6.00	6,439	3.92	16.59	11.37	7.56	0-16*a*
1,052	966	985	4,962	6.46	4,876	4.45	4,895	2.98	14.03	9.82	6.62	0-17*a*
2,589	2,269	2,184	9,219	12.04	8,899	8.13	8,814	5.37	19.61	13.50	9.01	0-18*a*
1,046	985	1,008	4,199	2.74	4,138	1.89	4,161	1.27	10.31	7.26	4.91	0-19*a*
1,260	1,204	1,281	4,592	3.00	4,535	2.07	4,612	1.41	10.57	7.44	5.05	0-20*a*
1,626	1,517	1,567	6,145	4.00	6,036	2.75	6,086	1.85	11.57	8.12	5.49	0-22*a*
1,389	1,295	1,334	6,461	4.22	6,367	2.91	6,406	1.95	11.79	8.28	5.59	0-23*a*
2,800	2,574	2,614	10,381	6.76	10,155	4.63	10,195	3.10	14.33	10.00	6.74	0-24*a*
3,670	3,318	3,310	14,568	9.50	14,216	6.49	14,208	4.33	17.07	11.86	7.97	0-25*a*
1,541	1,400	1,399	6,210	2.53	6,069	1.73	6,068	1.16	10.10	7.10	4.80	0-26*a*
1,320	1,168	1,136	6,736	2.75	6,583	1.88	6,551	1.25	10.82	7.23	4.89	0-27*a*
1,976	1,820	1,855	7,440	3.03	7,284	2.08	7,319	1.39	10.60	7.45	5.03	0-28*a*
1,474	1,332	1,339	7,203	2.94	7,060	2.02	7,058	1.34	10.51	7.39	4.98	0-29*a*
1,974	1,868	1,960	8,622	3.51	8,517	2.43	8,608	1.64	11.08	7.80	5.28	0-30*a*
2,861	2,672	2,764	11,507	4.68	11,318	3.23	11,410	2.17	12.25	8.60	5.81	0-31*a*
4,367	4,087	4,218	18,577	7.58	18,297	5.22	18,428	3.50	15.15	10.59	7.14	0-32*a*

The annual operating expenses cover the following two items: maintenance and operation, and cost of energy loss. The annual expenses for maintenance and operation are taken as a percentage of the system capital cost, varying with each of the six major groups forming the capital cost, and are given in Table VI.

The cost of energy loss per kilowatt-hour is taken equal to the sum of the cost of generation and the cost of transmission. The calculations are made for annual load factors of 35, 50, and 75 per cent.

The capital cost of the hydroelectric power plant is taken at $200 per kilowatt for all systems under design. The annual fixed charges of the power plant are taken at 10.323 per cent, which is the rate used for the equipment-comparative-increment-cost item of the system. The annual maintenance expenses are taken at one per cent of the capital cost per annum, and the cost of operating labor is taken at 0.20 mil per kilowatt-hour. At these charges the cost of generation at 35, 50, and 75 per cent annual load factors is correspondingly 7.57, 5.37, and 3.64 mils per kilowatt-hour.

The cost of transmission per kilowatt-hour, taken in determining the cost of energy loss, is calculated at the corre-

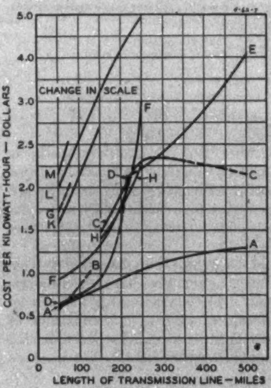


Figure 7. Cost of the entire system per kilowatt-hour at 50 per cent annual load factor, with all costs reduced to one tower line

For explanation of identification letters, see Figure 2

The shape of curve *C* is due to the fact that the 500-mile system (0-25*a*) has three tower lines, of which two are on a common right of way and the third on a separate. The other systems represented by this curve have one tower line per right of way

Table VIII. Division of Annual System Cost Into Major Component Parts at 50 Per Cent Annual Load Factor

In Per Cent of Total Annual System Cost

System Number	Fixed Charges (Per Cent)	Expenses for Maintenance and Operation (Per Cent)	Cost of Energy Loss (Per Cent)
0-1*a*	58.7	9.8	31.5
0-2*a*	65.7	11.3	23.0
0-3*a*	60.9	9.8	29.3
0-4*a*	64.0	10.6	25.4
0-5*a*	67.1	10.2	22.7
0-6*a*	66.6	9.7	23.7
0-7*a*	60.8	10.7	28.5
0-8*a*	58.8	10.1	31.1
0-9*a*	63.0	10.1	26.9
0-10*a*	72.3	11.4	16.3
0-11*a*	66.7	10.6	22.7
0-12*a*	62.2	11.3	26.5
0-13*a*	66.8	12.1	21.1
0-14*a*	65.5	11.6	22.9
0-15*a*	63.3	10.8	25.9
0-16*a*	59.7	10.5	29.8
0-17*a*	68.6	11.5	19.9
0-18*a*	64.0	10.5	25.5
0-19*a*	64.3	11.9	23.8
0-20*a*	62.0	11.5	26.5
0-22*a*	63.7	11.2	25.1
0-23*a*	67.4	12.3	20.3
0-24*a*	63.3	11.3	25.4
0-25*a*	65.7	11.0	23.3
0-26*a*	64.8	12.1	23.1
0-27*a*	69.2	13.1	17.7
0-28*a*	63.2	11.8	25.0
0-29*a*	68.4	12.7	18.9
0-30*a*	66.2	11.8	22.0
0-31*a*	64.9	11.5	23.6
0-32*a*	66.7	11.0	22.3

Table IX. Typical Calculation of Total System Capital Cost—System 0-16a

	Cost of Item as Delivered (Material Cost) (Dollars)	Cost of Item Installed (on Foundation Where Needed) Including Labor Cost (Dollars)	Group Cost (Dollars)
1. Equipment Comparative Increment			
1.1 water turbines	340,700	385,000	
1.2 waterwheel generators	3,322,500	3,754,400	
1.3 power-house superstructure		1,695,000	
1.4 shunt reactors on generator bus	405,600	438,500	
1.5 addition to structure, to house shunt reactors		72,800	
		6,345,700	
Indirect expenses		1,427,800	
Cost of money during construction		233,200	8,006,700
2. Sending Substation			
2.1 land	17,500	17,500	
2.2 structures		349,300	
2.3 power transformers	1,042,000	1,241,000	
2.31 resistors in transformer neutral	107,900	121,000	
2.4 high-voltage switching and protective equipment			
2.41 oil circuit breakers	757,600	901,000	
2.42 air-break disconnectors	141,600	168,500	
2.43 lightning arresters	12,800	15,250	
2.44 busses and supports		155,300	
2.5 low-voltage switching, protective, and control equipment		679,000	
2.6 auxiliary substation equipment		213,600	
		3,861,450	
Indirect expenses		868,850	
Cost of money during construction		141,900	4,872,200
3. Line			
3.1 clearing brush and trimming trees		435,600	
3.2 supporting structures		2,920,600	
3.3 overhead conductors	2,636,800	2,636,800	
3.4 ground wires	495,400	495,400	
3.5 Insulators and hardware			
3.51 insulators	222,900	222,900	
3.52 hardware	12,150	12,150	
3.6 counterpoise and tower-footing resistance			
3.61 counterpoise	94,800	94,800	
3.62 plowing counterpoise and arranging tower-footing resistance		31,400	
3.7 equipment for communication and relaying	75,450	89,700	
		6,939,350	
Indirect expenses		1,561,350	
Cost of money during construction		255,000	8,755,700
4. Right of Way			
4.1 cost of right of way	1,960,000	1,960,000	
Cost of money during construction		58,800	2,018,800

sponding annual load factor from the sum of the annual fixed charges and annual expenses for maintenance and operation of the entire system.

The total annual kilowatt-hours of losses are the sum of losses independent of the load and those dependent on it. With this in mind, the two groups of losses are calculated separately for various system parts, as discussed in the appendix. The annual kilowatt-hours of losses dependent on the load are determined for the three annual load factors by the application of the loss factor calculated for the corresponding annual load factor.[14,15] It is believed that this method gives more uniform results through eliminating assumptions associated with load curves, should these have been used.

The total cost of energy loss for each system at a certain annual load factor is calculated from the total number of annual kilowatt-hours of losses and the cost of energy loss per kilowatt-hour at the corresponding annual load factor.

System Annual Cost

The total annual system cost at 35, 50, and 75 per cent annual load factors is given in Table VII. The cost per kilowatt-hour of the entire system and of the transmission line alone, at a 50 per cent annual load factor, is represented in Figures 5 and 6. Figure 7 gives the kilowatt-hour cost of the entire system at a 50 per cent annual load factor with total costs reduced to one tower line.

The shape of the kilowatt-hour cost curves is similar to that of the capital-cost curves because the annual fixed charges constitute the largest part of the total annual cost. In view of this, the same conclusions may be drawn from the annual-cost curves as those drawn from the capital-cost curves. The kilowatt-hour-cost curves permit the estimating of the kilowatt-hour cost for systems other than those reported in the paper. It will be observed that these curves have a sharper bend when drawn for the transmission line alone than when plotted for the entire system, especially with smaller blocks of power transmitted. This is due to the fact that the losses in the transmission line form the major portion of all the system losses and that the ratio of line losses dependent on the load to those independent of it is different from the corresponding ratio of losses in the entire system.

In two pairs of systems, namely 0-1a, 0-2a, and 0-28a, 0-29a, transmitting equal blocks of power over equal distances at different voltages, the lowest annual costs and the lowest capital costs occur at different voltages. This may be the result of accumulated errors, or may represent those infrequent cases where the minimum capital and annual costs do not coincide.

Table VI gives also the cost of the kilowatt-hour at the receiving end of the system. This is the sum of the cost of generation, the cost of transmission as calculated for the purpose of determining cost of energy loss, and the cost of energy loss.

Division of System Annual Cost Into Its Component Parts

The division of system annual cost into its three major component parts is given in Table VIII for the 50 per cent annual load factor. At 35 and 75 per cent load factors the division of the annual cost is very similar to that at the 50 per cent load factor.

The fixed charges form the major part of the total annual cost. The division of the fixed charges into their four components is similar for all systems and is as follows: The taxes form 20.3 per cent, the insurance 3.4, the depreciation and obsolescence 15.4, and the interest on money invested 60.9 per cent.

Any changes which may take place in the capital cost may be introduced into the annual cost through the percentages which the fixed charges form of the total annual cost. Similarly, the effect of changes which it may be desired to make in the rate of taxes, or in other components of the fixed charges, may be readily evaluated. For example, in the systems under study taxes are taken at two per cent of the capital cost, which is 20.3 per cent of the annual fixed charges. If it is assumed that in a particular system the annual fixed charges are 60 per cent of the total annual cost, the annual cost of taxes amounts to about 12 per cent of the

Typical Calculation of Total System Capital Cost—System O-16a (Continued)

	Cost of Item as Delivered (Material Cost) (Dollars)	Cost of Item Installed (on Foundation Where Needed) Including Labor Cost (Dollars)	Group Cost (Dollars)
5. Intermediate-Condenser and Sectionalizing Substations			
5.1 land	2,500	2,500	
5.2 structures		216,000	
5.3 power transformers	1,188,000	1,415,000	
5.4 synchronous condensers	2,011,900	3,621,400	
5.5 high-voltage switching equipment			
5.51 oil circuit breakers			
5.52 air-break disconnectors	11,800	14,000	
5.53 busses and supports		96,000	
5.6 low-voltage switching, protective and control equipment		252,000	
5.7 auxiliary substation equipment		132,000	
		5,749,000	
Indirect expenses		1,293,500	
Cost of money during construction		211,300	7,253,800
6. Receiving Substation			
6.1 land	17,500	17,500	
6.2 structures		349,300	
6.3 power transformers	1,408,000	1,676,000	
6.4 synchronous condensers	1,296,800	2,334,200	
6.5 high-voltage switching and protective equipment			
6.51 oil circuit breakers	757,600	901,000	
6.52 air-break disconnectors	141,600	168,500	
6.53 lightning arresters	12,800	15,250	
6.54 busses and supports		155,300	
6.6 low-voltage switching, protective and control equipment		582,000	
6.7 auxiliary substation equipment		218,600	
		6,412,650	
Indirect expenses		1,442,850	
Cost of money during construction		235,700	8,091,200
Total System Capital Cost			38,998,400

System 0-16*a* transmits 250,000 kw over a distance of 250 miles, at a voltage of 230 kv, over two circuits on two separate and remote rights of way.

Construction period taken at one year. Cost of money during construction taken at six per cent for one-half construction period.

Cost of stringing overhead conductors and ground wires, cost of hanging suspension insulation and attaching hardware (labor cost) are included in total cost of supporting structures as installed on foundation.

System 0-16*a* is essentially an uneconomical system, as may be seen from Table VI of reference 8, where are outlined all the means necessary to keep the system stable in the transient state. This is reflected in the high capital cost of the "equipment comparative increment" item.

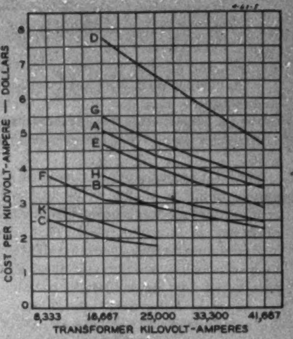


Figure 8. Cost of single-phase power transformers

Sending transformer, delta grounded wye:

A—13.8/345 kv *B*—13.8/230 kv
C—13.8/138 kv

Receiving transformer, grounded wye-delta-wye:

D—330/16.5/66 kv
E—220/16.5 or 13.8/33 kv
F—132/16.5 or 13.8/22 kv

Transformer for intermediate synchronous condenser, grounded wye-delta:

G—340-335/16.5 or 13.8 kv
H—225/16.5 kv *K*—135/13.8 kv

All transformers are oil-insulated self-cooled. The receiving-end transformers have ±10 per cent load-ratio control. Their main-winding kilovolt-amperes are used in the figure

total annual cost. Should the tax rate double, the cost per kilowatt-hour would increase by about 12 per cent. The percentage method of the division of the annual cost into its component parts may also be used to evaluate the effect of maintenance and operation costs, as well as energy-loss costs, when these differ from those taken in the study. Even a different cost of generation may be introduced and its effect on the kilowatt-hour cost determined.

The 345-Kv Systems

Study of the 345-kv systems as a separate group shows that the economics of these systems is a function of both the block of power and the transmission distance.

The larger the block of power, the shorter the distance at which the 345-kv transmission voltage becomes more economical. For example, at distances under 100 miles the block of power must be of the order of 800,000 kw if 345 kv is to be the most economical voltage; at distances approaching 200 miles, 345 kv becomes economical when 500,000 kw is transmitted; and when the transmission is over distances exceeding 200 miles, 250,000 kw may already be transmitted at 345 kv at a cost smaller than at 230 kv.

There are several cases of transmission of large blocks of power over long distances for which no comparison between the 345-kv and the 230-kv voltages may be made. This is when the system is unstable transmitting any reasonably large block of power at 230 kv, or when stability could be attained at a cost very much greater than that obtainable with 345 kv.

When transmitting with 345 kv, the cost of substations per kilowatt always exceeds the corresponding cost at the 230-kv voltage. The economy, however, of the 345-kv systems is due to the lower cost per mile of the transmission line itself, as well as to the smaller equipment comparative increment cost. In the 345-kv systems this item of the capital cost structure is either very small or entirely nonexistent, because of the stability being so much more easily attained in these systems.

Insofar as the total weight, and hence the total cost, of the supporting structures for the 345-kv systems is concerned, it is not appreciably greater than that for the 230-kv systems, when equal blocks of power over equal distances are transmitted under identical conditions of design and reliability.

It is entirely possible that when a 345-kv transmission project becomes an actual business proposition, the 345-kv equipment may be obtained at a cost lower than that taken in this study, thus increasing the savings obtained from the use of the 345-kv systems and extending the range of their economic application.

Effect of Design Conditions and Economic Factors on System Cost

The various design conditions, technological assumptions, and economic factors involved in arriving at specific system costs could have been modified in different ways, either separately or simultaneously. It is impractical and even impossible to estimate the possible ways in which the various modifications of adopted methods and factors, or their combinations, could affect the system costs. A general interpretation of the problem, however, can be given.

The most important of all conditions of system design is that of reliability and continuity of service, coupled with the requirement of proper operating facilities. The minimum number of circuits allowed, their sectionalization, the type, place, and duration of fault, as well as the question of a separate or common right of way—much as these affect the total cost—are of less importance from the economic viewpoint than might appear at a first glance. One single-circuit tower line will require less copper than two tower lines, and the total cost of its supporting structures and of the right of way will be smaller. On the other hand, ultrahigh-speed reclosing circuit breakers will be required and the necessity of interconnection with other systems will become imperative. In systems with one tower line where one circuit would replace two, the number of towers would probably not be reduced, their height might have to be greater and the right of way wider, and the ultimate economy would be questionable. The less stringent conditions of reliability for the light and short transmission systems will affect the total cost very little. In the larger and longer systems the saving will be somewhat greater, but it will be obtained at the cost of greatly impaired operating results and facilities.

In the same class of design conditions affecting the system cost are the high-voltage busses, their sectionalization, receiving-end synchronous condensers, self- instead of water-cooled power transformers, and the provision for a certain amount of spare equipment. Any of these might be changed in order to lower the system cost, but this would not be good operating practice. In some cases the use of voltages such as 115 kv and 161 kv might possibly slightly reduce the total cost, but this might not always be the case. The use of wooden supporting structures would affect the cost considerably, but this is a matter of choice.

In a few systems the total costs could be slightly reduced by installing a somewhat greater kilovolt-ampere capacity of generators and transformers, so that the copper in the line would be utilized to the fullest extent. This would increase the system rating. The resulting decrease in the cost per kilowatt or per kilowatt-hour, however, will not be appreciable.

The economic factors affecting the system cost are: prices of basic materials, cost of equipment, cost of labor and installation, cost of the right of way, financial methods of cost estimates, annual fixed charges, and cost of operation. Any, or all, of these may vary from those taken in the study. The only manner in which their effect on system cost may be estimated is through the medium of equipment-cost curves and through the use of tables giving system equipment and the percentage division of capital and annual costs into their component parts.

In estimating the magnitude of error in the final cost figures, it will be seen that its sources are in the system design and the cost estimates. In various system parts the two errors may lie in the same or opposite directions. In the former case the resulting error is their sum, and in the latter—the difference. The error in the design of any system part is considerably below ten per cent, since the system characteristics were studied on the network analyzer. The error in the cost figures is probably around ten per cent. The equipment costs contain the usual error present in all quotations obtained from the manufacturers; it is somewhat

Table X. System Losses

	Losses Independent of Load in Kilowatts						Losses Dependent on Load in Kilowatts						
System Identification Number	Corona	Sending and Receiving Transformers	Receiving-End Synchronous Condensers	Intermediate Synchronous Condensers	Other Losses*	Total Losses	Line	Sending and Receiving Transformers (Main Winding Only)	Receiving Transformers (Third Winding)	Receiving-End Synchronous Condensers	Intermediate Synchronous Condensers	Other Losses*	Total Losses
0-1a	52	307	480		48	887	3,820	460		480		148	4,908
0-2a	89	292	370			751	1,654	437	181	370		100	2,742
0-3a	60	325	510		75	970	3,840	486		510		175	5,011
0-4a	134	297	430			861	2,525	445	216	430		100	3,716
0-5a	489	295	330			1,114	2,690	441	160	330		100	3,721
0-6a	650	299	290	320		1,559	3,770	449	136	290	320	100	5,065
0-7a	89	587	710			1,386	4,170	881	404	710		200	6,365
0-8a	245	599	860			1,704	5,710	899	466	860		200	8,135
0-9a	260	603	830		68	1,761	7,300	906	448	830		268	9,752
0-10a	225	592	450			1,267	3,415	880	233	450		200	5,187
0-11a	375	603	490	1,075		2,543	7,070	906	259	490	1,075	200	10,000
0-12a	36	1,507	2,260		209	4,012	7,470	2,263	1,263	2,260		709	13,965
0-13a	75	1,449	1,820			3,344	6,150	2,175	994	1,820		500	11,639
0-14a	113	1,472	1,940			3,525	9,065	2,212	1,072	1,940		500	14,789
0-15a	225	1,595	2,640			4,460	15,700	2,397	1,492	2,640		500	22,729
0-16a	1,035	1,518	2,160	3,320	5,645	13,678	14,150	2,280	1,208	2,160	3,320	6,695	29,813
0-17a	305	1,475	1,600	2,040		5,420	13,170	2,216	856	1,600	2,040	500	21,382
0-18a	610	1,503	1,680	7,840	295	11,928	19,700	2,259	905	1,680	7,840	1,090	33,114
0-19a	113	2,935	3,760			6,808	18,160	4,402	2,057	3,760		1,000	29,379
0-20a	169	2,978	4,040			7,187	27,450	4,460	2,225	4,040		1,000	39,175
0-22a	774	3,170	5,160		566	9,670	27,450	4,754	2,915	5,160		1,566	41,845
0-23a	183	3,019	4,800			8,002	21,400	4,530	2,700	4,800		1,000	34,430
0-24a	305	3,066	4,120	5,800	1,335	14,626	37,700	4,600	2,276	4,120	5,800	2,865	57,361
0-25a	915	3,009	3,200	9,120	920	17,164	38,800	4,515	1,736	3,200	9,120	2,530	59,901
0-26a	216	4,694	6,080		173	11,163	20,500	7,035	3,460	6,080		1,773	38,848
0-27a	92	4,640	5,160			9,892	12,180	6,960	2,915	5,160		1,600	28,815
0-28a	275	4,870	7,080		940	13,165	30,750	7,305	4,065	7,080		2,540	51,690
0-29a	137	4,660	5,480			10,277	18,000	6,990	3,110	5,480		1,600	35,180
0-30a	275	4,769	6,440			11,484	36,000	7,150	3,685	6,440		1,600	54,875
0-31a	458	4,794	5,760	3,740	1,325	16,077	46,850	7,190	3,270	5,760	3,740	3,455	70,265
0-32a	1,220	4,835	4,560	8,140	1,660	20,415	62,100	7,255	2,550	4,560	8,140	3,920	88,525

* These include losses in shunt reactors on generator bus, in the increment generator kilovolt-amperes required by the shunt reactors, and in the current-limiting reactors.

greater only where the 345-kv equipment is involved. The error in estimating labor costs, costs of installation, and costs of miscellaneous substation equipment is probably close to 15 per cent. If one considers that these costs are obtained from study of actual systems and that in themselves they form only a part of the total system cost, it will be seen that the error is well within the limits of allowable engineering accuracy.

Summary and Conclusions

Results of the study may be summarized as follows:

1. The magnitude of the block of power, the transmission distance, and the voltage of transmission are economically interrelated. Transmission of a certain block of power at a certain voltage requires a definite distance of transmission if the most economical results are to be obtained for the system as a whole. Similarly when transmission is over a certain distance at a certain voltage, there is a definite magnitude of the block of power that can be transmitted most economically. The voltage of transmission over a certain specific distance depends on the block of power transmitted: for an over-all economical transmission, the voltage must increase to the next standard value as the block of power increases at certain intervals. These conclusions follow from both the capital and the annual cost figures.

2. As the block of power transmitted increases, with a simultaneous increase in the transmission distance, the cost per mile of the entire transmission system decreases at a rate greater than the rate of voltage increase.

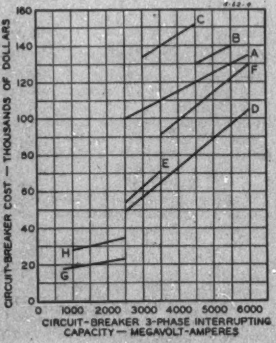


Figure 9. Cost of three-phase oil circuit breakers

A.	345–8	*E.*	230–6
B.	345–6	*F.*	230–4 or 3
C.	345–4 or 3	*G.*	138–8
D.	230–8	*H.*	138–4 or 3

First figure represents circuit-breaker continuous operating voltage; the second, its interrupting time

3. For the highest economy of operation, the transmission system must carry a load which is as close to the load for which it is designed as possible. Therefore the system must be designed for the block of power which is fully marketable either immediately or in the near future.

4. The curves given may be used to determine the most economical transmission voltage, distance, or block of power within the range investigated. They may be interpolated for voltages other than those used in the study. Both capital and annual costs may be estimated from the curves at the basic costs and economic factors assumed.

5. Study of the cost of various means employed for the increase of system transient-stability limit, as reflected in the increased system rating, shows that this expenditure is amply justified, in every case greatly decreasing the cost of kilowatt transmitted. Of all the means available for this purpose, the high-speed circuit breaker attains the result at the lowest cost. The means for increasing the system rating by raising its transient-stability limit, however, are to be employed only when the additional kilowatts that may be delivered can find a ready market. In systems operating at a voltage which is too low in comparison with the distance of transmission or with the block of power transmitted, the cost of stability improvement is so high as to make it more economical to resort to a higher transmission voltage.

6. There is a certain region of kilowatt-mileage combinations where the transmission voltage of 345 kv is decidedly more economical than the 230-kv voltage. If ever the cost of the 345-kv equipment becomes lower than that taken in the study, the region of the economical application of the 345-kv voltage will be extended beyond the range indicated in this study.

7. Tables are given showing the division on a percentage basis of the system capital and annual costs into their major component parts. Tables also show the amount of various equipment used in the system, by number or by weight of material. When costs other than those used in this study are considered for system equipment, for labor and installation, for the right-of-way or other system parts; when basic prices of steel and copper change; or when it is desired to modify some of the factors affecting the capital or annual cost structure—all of these modifications may be introduced through the use of the percentage-cost and the equipment tables. Similarly the tables may be used for estimating the cost when it is desired to modify the design of some system parts or to change the amount of equipment used, at the same time retaining the costs and economic assumptions of the study. Thus the cost of the system may be easily recalculated, and the effect of resulting changes on the cost per kilowatt, per mile, or per kilowatt-hour may be estimated without making a new design or cost calculation.

8. It is emphasized that for an exact determination of capital or annual costs of an actual system, these must be calculated with all the necessary detail. In some cases, with a certain combination of various factors, costs lower or higher than those reached in this study may be obtained. It is believed, however, that the relative costs and the effect of various factors will remain as presented.

9. In view of the economic advantages the 345-kv systems have in numerous instances over the 230-kv systems, the belief is expressed that the development of 345-kv equipment should begin. The engineering principles for this equipment will be essentially the same as for lower voltages, and the problem should present no extraordinary difficulties.

Appendix

1. The costs of 345-kv power transformers, oil circuit breakers for various opening times, air-break disconnectors, and lightning arresters are given in Figures 8, 9, and 10, jointly with costs of the corresponding equipment for lower voltages.

The type of grounding and the higher-than-normal voltage of the secondary winding of the transformers are accounted for in their cost. When transformers are required to operate for two hours with a 25 per cent overload, their price is increased by 12.5 per cent for the 230-kv units, and by 15 per cent for the 330- to 345-kv transformers. Prices of transformers of non-standard kilovolt-ampere sizes are determined by interpolation between adjacent standard sizes.

Prices of three-phase oil circuit breakers with lower than eight-cycle opening time, as well as those with an interrupting capacity higher than at present manufactured, are averages obtained from various manufacturers.

Prices of lightning arresters are for Thyrite-type units for grounded-neutral circuits.

In a few cases prices of the 345-kv equipment are obtained by extrapolation of prices for 230- or 287-kv equipment. All prices of

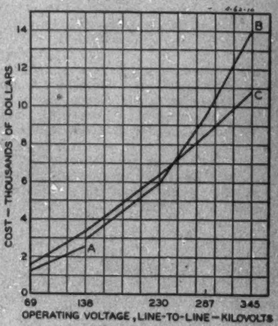


Figure 10. Cost of air-break disconnecting switches and lightning arresters

A. 600-ampere air-break disconnector
B. 1,200-ampere air-break disconnector
C. Three-phase lightning arrester, grounded neutral

Air-break disconnectors are three-pole outdoor motor-operated the 345-kv equipment contain from 10 to 15 per cent allowance for development costs.

2. All systems are equipped with carrier-current apparatus for both communication and relaying purposes. The carrier-current equipment for communication is a single-frequency "Duplex," with a bus coupling at both ends of the system through capacitors with potential devices. Carrier-current apparatus for relaying is connected directly to the line and is duplicated for each transmission-line circuit. Each sectionalizing station is equipped with a "Simplex" transmitter-receiver for communication, connected also directly to the bus. Systems 75 miles and longer are provided with patrolman sets for communication purposes; they are coupled permanently to the line and duplicated for lines on the separate right of way.

All prices, including those for the 345-kv systems, were obtained from manufacturers.

3. A typical calculation of the total system capital cost is given in Table IX. The table gives the six major parts of the capital-cost structure and their subdivisions.

4. System losses are given in Table X. All losses are divided into those independent of the load and those dependent on it.

The corona loss in the line is calculated by the Carroll-Rockwell method. The I^2R losses in the line are calculated from currents actually measured on the a-c network analyzer. The losses in the line are greater than in other system parts. In long and heavy systems the line efficiency is actually controlled by requirements of reliability and service continuity, which require a certain system voltage, a definite conductor diameter, and a certain amount of copper.

The generator losses are calculated only for the incremental generator kilovolt-ampere capacity, for pairs of systems transmitting equal blocks of power over equal distances at different voltages. The same procedure with regard to the incremental generator kilovolt-ampere capacity has been followed in the capital cost structure. Generator losses independent of the load and dependent on it are taken as equal.

Two fifths of the total losses in the sending transformer and in the main winding of the receiving transformer are taken as independent of the load; the remaining three fifths are copper losses dependent on the load. To the latter are added copper losses in the third winding of the receiving transformers.

Losses in the synchronous condensers, whether hydrogen- or air-cooled, are taken as supplied by manufacturers, and are divided equally between those dependent on the load and those independent of it.

In systems where shunt reactors are connected to the generator bus, it is assumed that 35 per cent of their capacity is permanently connected to the system; their losses therefore are independent of the load. Losses in the remaining shunt reactors depend on the load. These are air-core reactors, and their efficiency at 100 per cent load is taken at 99.6 per cent.

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Transformer Magnetizing Inrush Currents and Influence on System Operation

Discussion and authors' closure of paper 44-10 by L. F. Blume, G. Camilli, S. B. Farnham, and H. A. Peterson, presented at the AIEE winter technical meeting, New York, N. Y., January 24–28, 1944, and published in AIEE TRANSACTIONS, 1944, pages 366–75.

W. R. Brownlee (Commonwealth and Southern Corporation, Jackson, Mich.): The quantitative analysis presented of the many design and application factors governing transformer magnetizing inrush currents should be most useful from the standpoints of system planning and operation, not only in connection with units employing the newer high-permeability iron but for the solution of existing problems involving transformers with conventional iron.

The difficulties encountered in securing sensitive differential protection, even for the older units, are greater than is generally recognized, since the expedients introduced to eliminate false tripping on magnetizing current inadvertently may nullify the intended sensitivity. Sometimes current taps are raised, or restraining spring torque increased the required amount to prevent false tripping, and the reduction in sensitivity is neglected. The addition of devices to desensitize the relays for a short period following energization is almost as objectionable, because transformer faults are likely to occur when the unit is energized (as brought out by the authors), and because of the practice of many operating companies of making a thorough inspection of a transformer after a relay operation and then energizing the transformer once (if load is interrupted). Thus, protection is reduced at the very time when rapid sensitive operation is urgently needed. At first thought, the addition of an undervoltage cutout to a time-delay desensitizing device appears to offer a solution, until it is realized that faults requiring sensitive protection, such as those involving only a portion of a winding, usually will be accompanied by voltages high enough to defeat the undervoltage interlock.

From the authors' conclusion 2, it may be inferred that a certain number of incorrect relay operations on magnetizing current may be "tolerated." The present-day power transformer is a most reliable device, the failure rate due to all causes probably not exceeding one in five years, as contrasted with probably five to ten outages per year of typical subtransmission lines. This proved reliability is taken into account in system planning, so that only a small fraction of false relay operations on a transformer may be "tolerated," as compared with those associated with satisfactory line relay performance. Another way of viewing this ratio is that, after only one incorrect operation, transformer differential relays would have to render perfect performance for about a century in order to result in 95 per cent correct operation.

The magnitude of problems associated with transformer magnetizing inrush currents has been increased gravely by the use of transformers built with high-permeability iron. However, the striking reduction in materials thus made possible should result eventually in attractive economies to the users. It is hoped, therefore, that the study of an adequate solution of these problems will be given new impetus.

J. A. Elzi (Commonwealth and Southern Corporation, Jackson, Mich.): From the standpoint of transformer protection, either by means of fuses or relays, the energizing inrush current of transformers is of considerable importance. The number of cases in which troubles from this source have been experienced in the past has not been very great, but the trend in the use of low loss steel and corresponding higher normal flux densities can be expected to make it necessary to give this matter considerably more attention in the future. Apparently the maximum inrush current to be expected can be determined quite accurately in advance so that the protection engineer can have this information available when planning the protective scheme.

It is thought that the authors are somewhat overoptimistic in reporting that the relaying difficulties can be overcome by applying harmonic restraint relays or by using desensitizing devices. As mentioned in our discussion[1] of C. D. Hayward's paper, reference 10 of the paper, some harmonics of the inrush current are neutralized and others considerably reduced for certain commonly used transformer connections and associated current transformers, so that the desired restraint is not always available. Also, it is usually very important that the sensitivity of the relays be retained at the time of energizing a transformer bank.

With reference to the selection of fuses, the authors outline a method which will insure that the fuses will not be damaged by the inrush current. It is believed that some emphasis should be given to the fact that this refers to damaging the fuse and not to blowing it. Most published fuse curves give average blowing time, whereas, for this purpose, the minimum damaging time–current curves should be used. This, in many cases, will result in selecting considerably larger fuses than would be the case if average melting-time curves were used.

Reference

1. Discussion by J. A. Elzi and W. R. Brownlee of Harmonic-Current-Restrained Relays for Transformer Differential Protection. AIEE Transactions, volume 60, 1941, pages 623–4.

C. L. Schuck (General Electric Company, Philadelphia, Pa.): This paper gives a much-needed quantitative statement on the magnitude of magnetizing inrush current of power transformers, appraises the affecting factors, and shows the use of inrush data in application problems. Engineers interested in continuity of service will welcome the guidance afforded by this paper, so that, in making applications, a considerable percentage of outages formerly encountered may be eliminated.

The paper emphasizes the importance and usefulness of the I^2T parameter for evaluating inrushes of short duration as well as the ability of fuses and other series-connected devices to pass these inrush currents without damage to themselves. The I^2T concept has been made use of increasingly in power-system applications of fuses, in potential-transformer-fuse applications, in distribution-system co-ordination studies involving reclosers or repeating fuses, and also in welding circuits for which a meter has been developed for measuring ampere-squared seconds directly.

With reference to Table I of the paper, which shows the relative crest inrush currents for sequential versus simultaneous switching of transformers connected in various ways, it appears that these data would be more useful if expressed in the form of the maximum possible integrated ampere-squared seconds flowing through any phase conductor relative to the I^2T of the transformer in a single phase. Generally one fuse is used, one in each phase conductor leading to a power transformer bank, and, with the inrush data in the suggested form, it would be apparent whether sequential switching of three-phase banks would call for a higher-fuse-ampere rating than a single-phase transformer having the same primary full-load current.

E. M. Wood (Hydro-Electric Power Commission of Ontario, Toronto, Ont., Canada): On the large 25-cycle systems in Ontario the problem of the effects of magnetizing transients on the protection of transformers has been outstanding for many years. Although we have not yet seen any scheme which we think is perfect and, therefore, believe that there is need for further development, our practice for protection of medium and large transformers has evolved to the following.

For protection against incipient internal faults we install on each transformer unit a gas-pressure relay of the Bucholz-Kidde type. Our experience with these as to their effectiveness in clearing incipient winding faults and in detecting conditions, such as failure of corebolt insulation, has been very favorable.

While the form of relay we have used has some detail defects, if appreciated they do not reduce greatly the usefulness of the device. In several years' use we have had no case of incorrect tripping on external faults.

We supplement the gas-pressure relay with transformer-zone differential relays of usual type for high-speed clearance of heavy zone faults, either in or out of the transformer.

L. F. Blume, G. Camilli, S. B. Farnham, and H. A. Peterson: Mr. Brownlee's comments appear to be predicated on the assumption that a relay operation attending the transient inrush current that results when a transformer is energized necessarily creates an interruption to service. On this basis he questions whether any false relay operations can be tolerated.

Let us consider the typical case of a radial system with one transformer bank supplying a load. An actual interruption to service must necessarily already be in progress when an attempt is made to reenergize the transformer. Hence, if in a very few cases the inrush current is sufficient to cause an immediate tripout, it cannot be said that service was interrupted thereby. Actually, the principal consequence is only the embarrassment of having to close the breaker a second time. On the other hand, if the system is in the form of a network, there is not even an interruption to service when a transformer is switched out. Therefore, if a breaker trips on the first attempt to re-energize it, service is in no way affected, and the only serious consequence is, again, the embarrassment of having to make another attempt. This being the case, it is not a matter of service interruptions but only one of convenience that is being evaluated when one questions whether the tripping of differential relays during the inrush period can be tolerated. As has been pointed out in the paper, if such infrequent tripping cannot be tolerated, properly applied harmonic restraint relays offer a solution.

As Mr. Elzi states, it is true that, with certain transformer connections, some of the harmonic components of inrush current may be reduced in the relay circuits. Nevertheless, within the application range of the relay, as determined by the ratio of maximum internal fault current to minimum relay-operating value, the harmonic-restraint relay applied to protect a single transformer bank (as pointed out in the paper) will prevent operation on magnetizing inrush current.

Fuse-application practices, Mr. Elzi states, should prevent damage to the fuse links during the transient inrush period. An attempt was made to do this in the suggested criterion of "12 times normal current for 0.1 second." The actual inrush currents, as stated in the paper, were found to be substantially equivalent in their heating effect to ten times the normal full-load current of the transformer for 0.1 second. If data generally were available on the damaging time–current characteristics of fuses, the factor 10 could be used. However, since most published fuse data are based on melting time, the factor was raised to 12 in order to allow a margin which would permit the use of the available melting-time characteristics.

Mr. Schuck makes the suggestion that Table I of the paper, which illustrates the relative magnitudes of the peak inrush currents for various transformer connections, would be more useful if expressed in the form of integrated ampere-squared seconds. His purpose is to permit a direct comparison between the fuse sizes required for three-phase transformer banks and single-phase transformers having the same primary full-load current. It should be pointed out, however, that the data presented in Table I of the paper are entirely the relative magnitudes of the first peaks of the inrush-current transients for representative transformers, whereas, any evaluation of ampere-squared seconds must take into account not only the magnitudes of the first peaks, but also the rates of decay of the transients.

In general, the rates of decay will be different for the various physical sizes of the transformers involved, as is stated in the paper. Since Mr. Schuck's suggestion (which involves comparing three-phase transformer banks with single-phase units having the same full-load primary current) actually would result in a comparison of the inrush characteristics of transformers having different kilovolt-ampere ratings, and, therefore, different rates of decay, the generalized form of presenting data as used in Table I of the paper no longer could be utilized. Instead, it would appear necessary to list separately the inrush characteristics for each individual case, which would be entirely prohibitive.

Furthermore, by presenting the data in the form used in Table I, it is not restricted to fuse applications, and it is usable much more readily for indicating relay settings and the relative severity of the system-voltage dips that may result from inrush currents for the various transformer connections.

Current Rating of Cables as Affected by Mutual Heating in Air or Conduit

Discussion and closure of paper 44-53 by the Insulated Power Cable Engineers Association committee on research, presented at the AIEE winter technical meeting, New York, N. Y., January 24–28, 1944, and published in AIEE TRANSACTIONS, 1944, pages 354–65.

C. T. Hatcher (Consolidated Edison Company of New York, Inc., N. Y.): The authors of the paper should be complimented on obtaining and correlating data on a subject on which there has been a scarcity of actual test data.

It is noted that the conduits were grouped with what is specified as a standard spacing and that this is assumed to be the minimum spacing which will be used for the installation of conduits. In practical application of conduit installation, all conduits may not be installed with the same space separation. Therefore, the correction factors given will have to be considered as giving a loading which is the most favorable to a cable, whereas, in practical application the cables may be given a higher rating due to the increased space separation.

Our experience has been confined to relatively small conduit grouping such as 2×3 and in these cases the rating for the cables has been based on the envelope surface of the group of conduits. Values obtained from this method give an average rating for the conduits which results in ratings higher than those which would be allowed by the use of the authors' figures. For grouping of conduits the authors' correction factors will be of value in determining ratings.

Herman Halperin (Commonwealth Edison Company, Chicago, Ill.): The Insulated Power Cable Engineers Association certainly has made a substantial contribution in this paper. I have a few questions that follow:

1. In determining the current-carrying capacities, was the thermal resistance of the surface of the cable to conduit calculated so as to take into account the effect of contact of the cable with the conduit? That contact would cause a great reduction in the surface thermal resistance, particularly for a lead-sheathed cable in contact with an iron pipe. As brought out in my AIEE paper, "Load Ratings of Cable,"[1] the surface emissivity resistance for such cable, even in a concrete duct, is less than half of what one finds for a cable in air. Even for a cable with nonmetallic covering the surface thermal resistance in iron conduit is probably less than in open air.

2. For the nine-duct conduit banks at the left of Figures 17 and 18 and right of Figure 20 of the paper what is the explanation for the conduit temperature rises varying 15 or 20 per cent between the left-hand conduit and right-hand conduit in the middle horizontal row?

3. What was the reason for giving temperature rises usually above the temperature of an idle conduit instead of above the room temperature?

In the tests the conduits were horizontal, and the ends were closed. With open ends, especially for short runs, the temperature rises in service would be relatively smaller. Also, where conduits are vertical, the dissipation of heat is facilitated. The tests were made with still air in the room, and the presence of a slight wind in service would make for smaller temperature rises. For instance, in the AIEE paper, "Thermal Rating of Overhead Line Wire," by Myron Zucker,[2] it is shown that, for a bare wire carrying a given current with a wind velocity of only 2.4 miles per hour, the temperature rise is 60 per cent less than for the same wire when there was no draft in the room.

The data should be very helpful as guides in determining current-carrying capacities. However, each major application, I believe, should be studied to determine what modifying factors enter to justify higher current ratings than would be obtained by simple direct use of the paper.

REFERENCES

1. LOAD RATINGS OF CABLE, Herman Halperin. AIEE TRANSACTIONS, volume 58, 1939, October section, pages 535–53.

2. THERMAL RATING OF OVERHEAD LINE WIRE, Myron Zucker. AIEE TRANSACTIONS, volume 62 1943, July section, pages 501–07.

Wm. A. Del Mar (Phelps Dodge Copper Products Corporation, Yonkers, N. Y.): The Electrical Testing Laboratories' data are based on tests of six groupings of conduit. The tables prepared by the IPCEA committee, however, show 36 groupings.

When the data on the six groupings were studied, it became obvious that they were insufficient to permit reliable interpolation and extrapolation over the range of groupings required for practical design purposes.

The cost of making the requisite additional tests at ETL and the delay that would have been involved stood in the way of making a useful report.

The tests in the Habirshaw laboratories were designed to overcome this impasse. They were on a smaller scale and involved higher-conduit temperatures than would occur in normal practice, but they gave the shapes of the curves relating thermal resistance with the number of conduits in vertical and horizontal groups, thereby permitting the ETL data to be extrapolated with a very practical degree of accuracy.

Mr. Halperin has questioned the propriety of using the surface emissivity factor derived from sheaths in free air for the case of cables in conduits. He has found that the surface emissivity resistance is lower in underground ducts than in free air. This is undoubtedly true. However, it is not an important factor, either in the derivation of carrying capacity in underground ducts or in groups of conduits in air. In the former case, the duct factor is not the true thermal resistance of the duct and earth; but it is a factor which, added to the thermal resistance of a cable in free air, gives the thermal resistance of the cable in a buried duct.

In the latter case, that of cable in conduit, the correction factor K_c is the square root of the ratio of two summations of three thermal resistances each. The cable surface resistance R_s is one of the three in both numerator and denominator. Considerable change of R_s, therefore, after the division and square root processes will make but little change in K_c.

A. F. Gambitta (Phelps Dodge Copper Products Corporation, New York, N. Y.): The Insulated Power Cable Engineers Association, the Electrical Testing Laboratories, and the Habirshaw Laboratories have given in this paper a fine example of co-ordinated work by three groups, the results of which should be of great value in making the most economical layouts of wire and cable in power stations, substations, and large business or industrial buildings. For the most important cases the formulas using the correction factor K_c or K_a would be used; whereas, for the less important cases, Table IX of the paper would suffice.

However, in using Table IX, its limitation must be appreciated. The paper states that the current-rating correction factors given in Table IX hold only when cables or conduits are grouped closely together, and their values are conservative. It is stated further that the conductor size and the kind of insulation have little influence on these correction factors. Nothing, however, is said about the cable-voltage ratings. It seems to me that these correction factors are substantially correct for low-voltage cables, regardless of their conductor size and kind of insulation. For high-voltage cables, however, these correction factors are entirely too conservative. For instance, if we take the case of 500,000 circular-mil single-conductor paper-insulated cables installed in air in a group of 12 (three vertically and four horizontally), the correction factor given in Table IX is 0.70, regardless of cable-voltage rating.

On the other hand, calculations made in accordance with the procedure shown on page 358 of the paper and the conduit group correction factor G given in Table V, will give a current-rating correction factor for cable rated at 15 kv of 0.72, which is close enough to the value of 0.70 given in Table IX; but for cable rated at 69 kv, this factor becomes 0.83 which is entirely out of line.

It seems, therefore, that the effect of cable-voltage ratings should be stated clearly in the paper in order to avoid misunderstandings. This could be accomplished very well by inserting in the title of Table IX the words "low voltage" and by emphasizing it in the text of the paper.

Joseph Sticher (The Detroit Edison Company, Detroit, Mich.): The IPCEA paper gives a nice story of the experiments conducted at ETL and of the reduction of the results to practical applications. It gives a good outline of the reason for undertaking the study, a good presentation of the methods of attack to obtain a solution, shows a healthy aggressiveness on the part of the manufacturers, is fairly concise and complete, and certainly can be considered of great value in practical applications in the electrical-engineering profession.

There is, however, at least one item that might be questioned, that is, the extreme effort at simplification employed to make the results yield a single table of current-rating correction factors for various groupings of cables. It is entirely possible that this simplification leads to unnecessarily conservative ratings. This question could be settled if, instead of one table covering the entire field of application, several tables, each covering a given set of conditions, had been prepared from the results obtained. From a comparison of the various tables it could be determined then whether the use of a single table is justifiable. If it were found that more than one table is desirable, then savings to the consumer would result.

Sergius Vesselowsky (The Detroit Edison Company, Detroit, Mich.): The report of the IPCEA committee on research about the experiments made with the view of clarifying the important question, "Current Rating of Cables as Affected by Mutual Heating in Air or Conduit," represents a very important contribution to sound loading of cables. Since appearance of the "Underground Systems Reference Book,"[1] no work of similar nature has been attempted in this country in spite of many questions of burning interest and importance waiting for an experimental study.

The committee on research is to be congratulated for its job. It is very desirable that this work should be continued and expanded, embracing other questions of cable-loading practice. As a small but relatively important detail, the desire can be expressed that, in the setup of future experiments, the eventual possibility of the theoretical approach to the problems be taken into account wherever it is possible and not only immediate practical needs. For instance, certain temperature measurements would give basis for analytical predeterminations of loading characteristic, even if they are not needed by the practical engineer. This desire does not decrease the value of the experimental work done by the committee, because the data obtained from the specific problem, studied experimentally this time, do not lend themselves to an analytical approach.

REFERENCE

1. UNDERGROUND SYSTEMS REFERENCE BOOK. National Electric Light Association Standard, 1931.

Andrew Gemant (The Detroit Edison Company, Detroit, Mich.): This IPCEA paper is considered, as a whole, very good and most elaborate. Although a few tables are given, showing final temperatures, a few samples of whole temperature–time curves would have been useful. These curves would indicate whether stationary conditions were reached. This is not quite evident from the figures submitted.

It is not obvious from the material presented whether end effects of the cables had been eliminated. A few distribution curves of temperatures along the entire cable length would be desirable in order to clarify this point.

R. J. Wiseman (chairman, IPCEA committee on research): The spacing used for the groups of conduits was that obtainable with the standard fittings for the particular size of conduit, therefore, the minimum spacing. We would like to have made tests at various spacings as may occur, as stated by Mr. Hatcher; but, as our funds were limited, it was necessary to obtain the most essential data to indicate how grouping affects mutual heating. By using minimum spacing for tests, the mutual heating would be less for any greater spacing, and, therefore, the currents obtained for the minimum spacings would be the lowest values to use. Mr. Hatcher's experience with small conduit groupings indicating that, by using the envelope surface of the group of conduits, a higher rating than indicated in our results is possible may be explained on the basis that he may be using the temperature of the outer conduits; whereas, we take the hottest conduit in a group as the control conduit. If we took an outer conduit, we, too, would have higher current ratings.

In reply to Mr. Halperin's first question, we used the well-known formula for the thermal resistance for the surface of a cable, namely, $R_s = K/D$ where K varies from 2.06 to 4.11 for a cable diameter $D = 0$ to 1.75 inches and 4.11 for all diameters over 1.75 inches. In other words, we use the same formula for a conduit as that used for a cable in the air or in a duct. In a duct we have the drop from the surface of the cable to the air in the duct and also from the air to the duct itself. The same is true for a conduit. Several years ago this subject was studied by the IPCEA, and, with the meager information available, it was decided that it was best to take the emissivity constant for the covering in air, as we have always done, and use it for cables installed in ducts and conduits. Mr. Del Mar comments very nicely on this subject.

We are unable to explain the differences in temperature of the outside conduits for the grouping of nine as shown in Figures 12, 13, and 15. Unfortunately, we did not know this occurred until after all tests were completed.

In answer to Mr. Halperin's third question, the room temperature would have been a logical reference temperature, but the tests were conducted in a very large room which did not have a constant temperature. It was thought that an air temperature fairly near the conduits would represent more nearly the true conditions in a building or power station. Actually, the difference between the two locations amounted to a degree or so and did not influence any conclusions.

It is true, as Mr. Halperin states, that, with open ends and short runs, temperature rises in service would be smaller. It is not possible to cover every condition that may exist, but only the least favorable, knowing that any departure gives that much better factor of safety of operation or there is an emergency overload condition that may be used if warranted. We would have liked to have made tests with vertical runs of conduit and also the effect of wind, but as stated above, our funds were limited. It is hoped that someone will make such studies in the future. We all know how helpful it is to have a breeze blowing on a cable to dissipate the heat.

The data obtained by Mr. Del Mar in the Habirshaw Laboratories were most helpful in clearing up some questionable points that arose when we were studying the ETL data.

If it is desired to get an accurate value of current for cables in conduits, then, as Mr. Gambitta states, it is desirable that the formula to determine K_c or K_a be used; otherwise, Table IX of the paper is sufficiently accurate. When we started this investigation we had in mind cables installed in buildings or power stations. Normally, such cables are of relatively low-voltage operation. That is why we did not show calculations for over 12 kv. We are fortunate that the correction factor K_c is almost independent of voltage, as it simplifies our setting up tables that anyone can use. This results from the fact that, as the thermal resistance of the insulation increases, the surface thermal resistance decreases, so that the sum of the two terms almost stays constant for moderate changes in voltage rating. We agree with Mr. Gambitta that, to be accurate, it is desirable that we confine the application of Table IX to low-voltage cables, and use the formulas for calculating K_c or K_a for voltages over 15 kv.

Table I

	Temperature (Degrees Centigrade)
One foot from north end	49.4
Center of north section	50.4
Coupling between sections	49.8
Center of south section	49.3
One foot from south end	48.4

It is true, as stated by Mr. Sticher, that we employed simplification to an extreme degree and did so intentionally. The table was set up so that anyone who has to design conduit systems, such as an electrical contractor, would not have to use formulas to determine the correct conductor size or current rating for a specific conductor size. We have in mind the high degree of simplification with which the current ratings are tabulated in the National Electric Code. Some of us believe that it is overdone, but here also, it was intentional. We thought it was necessary to follow suit. Even so, we believe that the current ratings obtainable by the proposed methods are liberal, yet not conservative and on a much more rational basis than has been possible in the past.

We agree with Mr. Vesselowsky that our investigation of this subject is only the beginning. We hope that others will become interested and study the many factors suggested by Mr. Vesselowsky and Mr. Halperin.

In answer to Doctor Gemant's comments, the data collected by ETL are quite voluminous, and it was impossible to include them in our paper. We gave representative data to show the nature of the ETL data. The temperature rise used to plot the various curves for each loading and grouping is that found after steady-state condition occurred. The conduits were about 20 feet long, and the temperature rise recorded is for the center of the conduit where end effect is absent. We do have data showing the difference in temperature along the conduit as shown in Table I of this discussion for the four-inch conduit and a loading of 15 watts per conduit foot.

The IPCEA would like to express its appreciation for the favorable viewpoint taken by the discussers. It makes us confident that our efforts to clarify that which we have been guessing in the past have been worth while, and that their recommendations will be used when current ratings for cables in conduits in groups are being calculated.

The Accuracy of Measurements in Lumped R-C Cable Circuits as Used in the Study of Transient Heat Flow

Discussion and authors' closure of paper 44-55 by Victor Paschkis and Michael P. Heisler, presented at the AIEE winter technical meeting, New York, N. Y., January 24–28, 1944, and published in AIEE TRANSACTIONS, 1944, April section, pages 165–71.

E. L. Armi (Leeds and Northrup Company, Philadelphia, Pa.): As stated in part IV, section 1 of the paper, it would be desirable to have theoretical curves for comparison with the experimental results obtained. Fortunately, such curves can easily be plotted for the case illustrated in Figures 13 and 14 of the paper. Carslaw[1] and Byerly[2] give an expression for the temperature in an infinite slab whose faces are held at fixed temperatures and which has a known temperature distribution at the time $t=0$. If appropriate boundary values are used, the expression will read, in symbols of the paper under discussion:

$$\frac{e}{E}=(1-\xi)+\frac{2}{\pi}\sum_{n=1}^{n=\infty}\frac{(-1)^n}{u}e^{-\frac{n^2\pi^2 t}{RC}}\sin u\pi(1-\xi)$$

For $t/RC \ll 1$ it is permissible to use the picture of a semi-infinite solid instead of an infinite plate,[1,2] resulting in the simplified expression

$$\frac{e}{E}=1-\frac{2}{\sqrt{\pi}}\int_0^{\frac{\xi}{2}\sqrt{\frac{RC}{t}}}e^{-x^2}dx$$

A check of Paschkis and Heisler's curves for 12 sections in Figures 13 and 14 shows a quite satisfactory agreement with values obtained by theoretical calculations.

REFERENCES

1. THE CONDUCTION OF HEAT (second edition), Carslaw. London, England, 1921. Pages 67, 35.

2. FOURIER'S SERIES AND SPHERICAL HARMONICS (book), Byerly. Boston, Mass., 1893. Pages 108, 85.

N. E. Polster (Leeds and Northrup Company, Philadelphia, Pa.): If n equal capacitances C and resistances R_u are used in each of n sections of an electric network used to simulate a heat-flow system, the total capacitance and resistance are

$$\sum_1^n C=nC \tag{1}$$

and

$$\sum_1^n R_u=nR_u \tag{2}$$

respectively.

If the capacitor time constant is

$$\tau=R_cC \tag{3}$$

then

$$\frac{R_c}{R_u}=\frac{\tau}{R_uC}$$

$$=\frac{\tau n^2}{\sum_1^n R_u\sum_1^n C} \tag{4}$$

Equation 4 shows that R_c/R_u is proportional to n^2 for a constant τ. It follows from the curves of Figure 5 of the paper that the errors for various R_c/R_u are independent of the number of sections n.

Example: Let

$$\frac{R_c}{R_u}=1{,}000 \text{ for 14 sections}$$

Then

$$\frac{R_c}{R_u}=1{,}000\frac{(9)^2}{(14)^2}=414 \text{ for 9 sections}$$

If we refer to Figure 5 of the paper, *both* current ratios are 1.055.

The errors which limit the number of sections are caused by those conductances which shunt the capacitors and are of such a magnitude that they change τ. While it may not be possible to make τ independent of the magnitude of C, it should be possible to reduce the shunting conductances to make their effect negligible.

Victor Paschkis and **Michael P. Heisler:** It is gratifying to learn from Mr. Armi's discussion that he checked our Figures 13 and 14 and found satisfactory agreement between his calculated values and our ex-

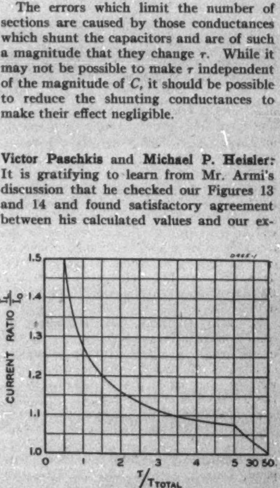


Figure 1

perimental values. In this connection, it may be mentioned that M. Avrami has developed formulas and graphs which permit checking not only Figures 13 and 14, but also Figures 11 and 12, that is, the charts which determine the temperature rise of walls having finite boundary resistances. His work has not yet been published. Based on his work some points on our Figures 11 through 14 had been checked mathematically and showed satisfactory agreement.

Mr. Polster's comments are of interest. Check on our Figure 5 shows that the three curves can be reduced to one new curve in which the current ratios again are plotted as ordinates and values of τ/T_{total} as abscissas.

$$\tau/T_{\text{total}}=R_u/(R_e\times n^3)$$

Following Mr. Polster's suggestion, we analyzed Figures 6 through 8 of the paper and find that these three figures can also be replaced by one figure. The new chart for current ratios is presented herewith in Figure 1, the new chart for voltage ratios in Figure 2.

That the several curves of Figure 5 can be combined into one curve is only an empirical fact and has no theoretical basis. It does not hold for any value of n and any value of R_e/R_u. This may be found by comparing the case for one and two sections. The current ratios for both these cases can be determined numerically without difficulty. With the notations of the paper, the calculation of the current ratio for one section can be made from the following equation:

$$\frac{I_L}{I_o}=\frac{\dfrac{R_u}{2}+\dfrac{1}{\dfrac{2}{R_u}+\dfrac{1}{R_e}}}{R_u} \tag{1}$$

For two sections having the same total resistance and total capacitance as the one section in equation 1 the equation reads as follows:

$$\frac{I_L}{I_o}=\frac{\dfrac{R_u}{4}+\dfrac{1}{\dfrac{1}{2R_e}+\dfrac{1}{\dfrac{R_u}{2}+\dfrac{1}{\dfrac{4}{R_u}+\dfrac{1}{2R_e}}}}}{R_u} \tag{2}$$

Note that in both cases τ and T_{total} are the same. It is evident that the two equations are not identical.

Further proof can be found by evaluating equations 1 and 2 separately for different values of τ/T_{total}. Equation 2 yields values which fit satisfactorily the general curve of Figure 1. The equation 1, however, yields values which are somewhat different.

These remarks should not detract from the importance of Mr. Polster's contribution. It is very helpful to be able (in the practical range between sections 5 and 14 and R_e/R_u=200–10,000) to reduce all curves to one single curve. The fact remains, however, that the equipment leakage (part III, section 2) is not covered by this simplification. The remark by Mr. Polster that "it should be possible to reduce the shunting conductances to make their effect negligible" may apply to some future development. For the present equipment, which is built with special thought to versatility, his assumption does not hold.

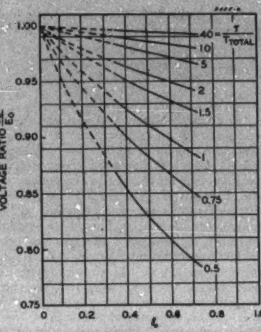


Figure 2

Symmetrical-Components Analysis of the Four-Phase System

Discussion and author's closure of paper A44-24 by A. Boyajian, presented at the AIEE winter technical meeting, New York, N. Y., January 24–28, 1944, and published in AIEE TRANSACTIONS, 1944, February section, pages 48–51.

A. N. Garin (General Electric Company, Pittsfield, Mass.): This paper is, in my opinion, a welcome and perhaps even an overdue addition to the still rapidly growing literature of the method of symmetrical components. Although the fundamental principles and even the formulas for expressing the current and voltage vectors of a four-phase system in terms of their symmetrical components, and vice versa, have been published quite some time ago, as indicated by the references given in the paper, this, so far as I know, is the first presentation of the formulas for the fault currents.

The author has invited comparisons between the three-phase and the four-phase techniques of symmetrical components. I should like to go further than that and compare the technique of sequence impedances with the old classical and recently much maligned technique of self- and mutual impedances. The well-deserved success of Doctor Fortescue's innovation has been so overwhelming that it has become customary to dismiss the alternative of the classical method of analysis with a few brief derogatory remarks, as for instance in this paper. And yet, I believe, the classical method can be of great help in clarifying the newer concepts, in adding another physical interpretation to their meaning, and quite often in facilitating the actual solution of a specific problem.

Any point in a symmetrical four-phase system is characterized by the four sequence impedances Z_1, Z_2, Z_3, Z_0. An alternative way of expressing the same characteristics is by means of the line-to-neutral self-impedances of the phases and mutual impedances between phases. Naturally, this method will also require the use of four quantities. They are:

1 The self-impedances

$$Z_{AA}=Z_{BB}=Z_{CC}=Z_{DD}=\frac{Z_1+Z_2+Z_3+Z_0}{4} \tag{1}$$

2. The mutual impedances between adjacent phases, taken in the positive-sequence direction

$$Z_{AB}=Z_{BC}=Z_{CD}=Z_{DA}=\frac{jZ_1-Z_2-jZ_3+Z_0}{4} \tag{2}$$

3. The mutual impedances between adjacent phases, taken in the negative-sequence direction

$$Z_{AD}=Z_{DC}=Z_{CB}=Z_{BA}=\frac{-jZ_1-Z_2+jZ_3+Z_0}{4} \tag{3}$$

4. The mutual impedances between opposite phases

$$Z_{AC}=Z_{CA}=Z_{BD}=Z_{DB}=\frac{-Z_1+Z_2-Z_3+Z_0}{4} \tag{4}$$

The equality of the four impedances, identified by subscripts denoting the phases, in each one of the above equations is the criterion of the symmetry of the system. For a static circuit

$$Z_{AA}=Z_{BB}=Z_{CC}=Z_{DD}=\frac{2Z_1+Z_2+Z_0}{4} \tag{5}$$

$$Z_{AB}=Z_{BC}=Z_{CD}=Z_{DA}=Z_{AD}=Z_{DC}=Z_{CB}=Z_{BA}=\frac{-Z_2+Z_0}{4} \tag{6}$$

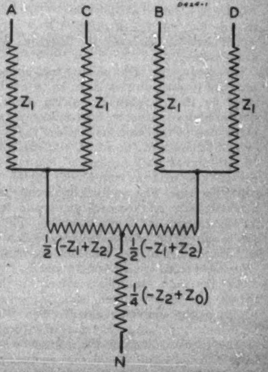


Figure 1. Equivalent circuit of a symmetrical static four-phase system with the line-to-neutral self- and mutual-impedances of the four phases expressed in terms of sequence impedances

$$Z_{AC}=Z_{CA}=Z_{BD}=Z_{DB}=\frac{-2Z_1+Z_2+Z_0}{4} \quad (7)$$

It will be observed that for a static circuit the j's have disappeared from all expressions, so that when Z_1, Z_2, Z_0 are pure reactances, the self- and mutual-impedances are also pure reactances.

At this point I should like to digress for a moment, to quote from the paper the statement that an attempt at analysis by the classical method was abandoned partly because the problem involved unknown mutual impedances. By whatever method the sequence impedances given in Table I of the paper were finally determined, whether by calculation or by test, if these impedances are

$Z_1=Z_3=8.8$ per cent
$Z_2=75$ per cent
$Z_0=150$ per cent

based on 15,000 kva and four phases, the self- and mutual-impedances are just as definitely known to be

Z_{AA}, etc. $=60.65$ per cent
Z_{AB}, etc. $=18.75$ per cent
Z_{AC}, etc. $=51.85$ per cent

based on 3,750 kva per phase.

Now let us calculate the line-to-ground fault determined in the paper. By Thevenin's theorem the fault current is given by the line-to-neutral voltage at the point of the fault, preceding the fault, divided by the line-to-neutral-self-impedance of one phase, that is

$$I_{\text{fault}}=\frac{100}{60.65}=1.65 \text{ times normal}$$

This, of course, is exactly the value obtained in the paper. The voltage induced by this current in the opposite phase obviously is given by

$$Z_{CA}I_A=Z_{CA}I_{\text{fault}}=51.85\times 1.65=85.7 \text{ per cent}$$

Since voltages *NA* and *NC* are reckoned in opposite directions, the voltage of phase *C* is given by 100+85.7=185.7 per cent, exactly as obtained in the paper. "Distasteful" as this method of solution may be, I would hesitate to call it "lengthy and burdensome."

Obviously, the self- and mutual-impedances of phases do not have to be calculated by the roundabout process of first determining the sequence impedances. Clear perception of the relations between the two sets of impedances, however, will expedite the solution of many problems. Figure 1 of the discussion shows these relations for a static system. The equivalent circuit has five terminals, one for each phase and one for the neutral, and seven branches. There are no mutual impedances between branches, and the values of the branches are easily obtainable from equations 5, 6, and 7.

All the fault conditions solved in the paper, and many others, can be solved readily by this equivalent circuit. Consider, for instance, the line-to-line fault of Figure 7 of the paper. Tracing the current path from the terminal *A* of the equivalent circuit to the terminal *B* we find that the impedance encountered is equal to

$$Z_1+{}^1/_2(-Z_1+Z_3)+{}^1/_2(-Z_1+Z_3)+Z_1 = Z_1+Z_3$$

For a fault of zero impedance, the fault current is given therefore by

$$I_{\text{fault}}=\frac{E_{BA}}{Z_1+Z_3}$$

which is equation 15b of the paper.

Only when voltages and currents at several points of an extensive system must be determned under conditions of unsymmetrical loading or during an unsymmetrical fault at some other point in the system will the method of symmetrical components display its full power and elegance. If the importance of existing four-phase systems warrants it, now that the ice has been broken, we should not have to wait too long for information on the properties of the networks of the four sequences, on their interconnections for various types of faults, and on the sequence impedances of four-phase apparatus. In general, it might be expected that a four-phase system would be linked at some point with a three-phase system. A complete investigation, therefore, must include a study of the laws of transformation of the sequence voltages and currents by the phase transformation links.

This paper should be of great value both in conducting further work on four-phase systems and in assimilating the results of such further work. For many years it will be the first item in the list of references on this new subdivision of symmetrical-components analysis.

A. W. Rankin (General Electric Company, Schenectady, N. Y.): The subject paper is a well-written and welcome addition to the technical literature on a-c systems, as the four-phase system is not so rare as the author's synopsis suggests. In the more ordinary three-phase systems, for instance, it is well known that large turbine generators are never built as true three-phase machines, but are built as six-phase machines and connected internally for three-phase operation; in a similar fashion, large turbine generators for two-phase systems really are built as four-phase machines. On some installations these four-phase generators are not connected internally for two-phase operation, but for switching reasons the terminals of all four phases are brought out to the generator terminal board. As a consequence, adequate protection of such machines must be based on four-phase analysis in order to insure proper relay operation for faults during the switching period. From this discussion, it is evident that Mr. Boyajian's analysis of the four-phase system will be used to a considerable extent, and his detailed results are very welcome.

The author has a great deal of logical justification for defining his four symmetrical systems as he does in Figures 1 to 4 inclusive, since such logic can be easily extended to n-phase machines. Nevertheless, to most engineers the subscripts 1, 2, and 0 immediately suggest the positive-, negative-, and zero-sequence phase orders respectively because of the much wider application of these subscripts in three-phase systems. For that reason, and with special emphasis on the character of the negative-sequence system which is important in the operation of turbine generators, it has seemed more practical to this writer to retain the subscript 2 for the negative-sequence and let the subscript 3 apply to the odd system in Figure 2 of the paper. With this method the 1, 2, and 0 subscripts have the same definition as in three-phase analysis, and higher subscripts automatically assume a special significance. The justification of this logic is seen when one considers that four-phase systems are not in too frequent use, and systems with a greater number of phases in even less frequent use. It is interesting to note that this latter notation has already been published by Edith Clarke;[1] in addition, Miss Clarke refers to the odd-component system (I_2 of the subject paper) as a "repeating positive-sequence two-vector system," which defines it completely. In the subsequent paragraphs the subscript notation previously outlined will be used, which differ from that of the author in the interchange of subscripts 2 and 3.

The expressions obtained by this writer on various four-phase short circuits agree completely with those of Mr. Boyajian, but with Z_1 replaced by x_d'' or x_d' depending on whether the subtransient or transient component is desired. It might be noted, however, that a short circuit of all four phases will give a short circuit current equal to

$$i=\frac{E}{x_d''} \quad (1)$$

which is the same as the value obtained for a short circuit of opposite lines of a four-phase turbine generator. This result could have been obtained by noting that adjacent phases are independent of each other because of their angular position. It is interesting to note further that for a short circuit of a single phase of a four-phase turbine generator in which $x_d''=x_2$, the result given by 9a of the subject paper

$$i=\frac{E}{\dfrac{x_d''+x_2+x_3+x_0}{4}} \quad (2)$$

may be put in the form

$$i=\frac{E}{\dfrac{x_d''+x_{0-3}}{2}} \quad (3)$$

in which x_{0-3} is the reactance obtained with zero-sequence currents in two opposite phases and zero currents in the two remaining phases. This can be verified by noting that the subscript-three system plus the subscript-zero system (Figures 2 and 4 respectively of the subject paper) will give zero current in the *b* and *d* phases, and double current in the *a* and *c* phases. Therefore

$$x_{0-3}=\frac{x_0+x_3}{2} \quad (4)$$

In conclusion, it has been observed that for a large four-phase turbine generator, the reactance x_3 is of the order of magnitude of $^1/_3$ to $^2/_3$ the direct-axis subtransient reactance.

REFERENCE

1. CIRCUIT ANALYSIS OF A-C POWER SYSTEMS (book), Edith Clarke. John Wiley and Sons, Inc., New York, N. Y., 1943. Volume 1, Symmetrical and Related Components, page 285.

A. Boyajian: Mr. Garin's spirited defense of the older methods provides perhaps a good balance to the present author's partiality to symmetrical components. Of course much depends on one's previous experience and taste, and what proves "distasteful" to a person is certain also to be very "burdensome" to him. Mr. Garin has obtained the present results very nicely, but may it not be said that, as Samson put it, he did a little ploughing with the author's heifer and that the rest of Mr. Garin's method (item *B* below) is not old by any means?

There are two valuable contributions in Mr. Garin's discussion:

A. In the symmetrical-components approach to a problem, it may be wise to raise the question whether that method should be carried to the "bitter end" (so to speak), or whether the work can be transformed back into the old system at some intermediate stage of the solution to good advantage. The most attractive feature of the symmetrical components approach is the ease with which the impedances can be obtained or estimated. With these on hand, it sometimes may be advantageous to derive from them the ordinary kind of impedance values and then proceed in terms of the actual circuit currents and voltages. This is the method Mr. Garin used in the first half of his discussion. He does say that "Obviously, the self- and mutual impedances of phases do not have to be calculated by the roundabout process of first determining the sequence impedances." Theoretically not; but I suspect that few engineers could do it the direct way and would prefer the "roundabout" way. Few tourists would pass up a well-paved roundabout route for an unpaved short cut.

B. In simple networks, Mr. Garin's equivalent network is valuable for calculating-board use, provided that it does not have negative links. The network is new, as far as I know, and cannot be credited to the old art in defense of it.

Mr. Rankin's statements about the turbine generator are interesting and the impedance data he has given valuable. As to the subscripts, I may be reconciled to the interchange of 2 and 3 by the authority of popular usage, but I will have to be cold to any forced interpretations of the novel component to justify it.

Polarized-Light Servo System

Discussion of paper 44-9 by T. M. Berry, presented at the AIEE winter technical meeting, New York, N. Y., January 24–28, 1944, and published in AIEE TRANSACTIONS, 1944, April section, pages 195–8.

F. J. Maginniss (General Electric Company, Schenectady, N. Y.): The polarized-light servo system described by Mr. Berry is an important part of the General Electric differential analyzer discussed by Peterson and Kuehni. This analyzer has now been in service for about eight months. Among the considerations which led to the choice of this system to replace the mechanical torque amplifier, used in previous analyzer design, two may be mentioned. They are reduction of required maintenance and elimination of feedback between output shaft and integrating wheel. In both of these respects, the polarized-light servo system has shown up very favorably.

The differential-analyzer application of this system subjects it to rather severe duty, more severe than that to which it is subjected by the Pressure Traverse Recorder. The follow-up system has to work equally well in both directions, speed accelerations may be high, and the input signal may not always be smooth or continuous. Despite this duty, the polarized-light follow-up system described in the paper, with some modifications to meet these particular demands, has shown itself to be accurate and dependable.

A New Differential Analyzer

Discussion and authors' closure of paper 44-16 by H. P. Kuehni and H. A. Peterson, presented at the AIEE winter technical meeting, New York, N. Y., January 24–28, 1944, and published in AIEE TRANSACTIONS, 1944, May section, pages 221–8.

C. N. Weygandt (Moore School of Electrical Engineering, University of Pennsylvania, Philadelphia, Pa.): To those of us at the Moore School of Electrical Engineering who have had eight years experience in the operation of a differential analyzer, a paper on a new differential analyzer is naturally of great interest. It is also very gratifying to us that this new differential analyzer is not so very different from our own.

The outstanding difference between the new General Electric Company analyzer and the one at the Moore School is the design of the integrating units. There are two major changes. First, the revolving disk on the new machine is fixed, and the wheel which rolls on the disk is moved by the lead screw which is proportional to the integrand. This arrangement reduces the friction load of the integrand lead screw very considerably. Since the lead screw loads are by far the largest on the Moore School analyzer, the new arrangement should give decidedly improved performance. The second new feature of the General Electric Company analyzer is the way in which the revolutions of the integrator wheel are coupled to the interconnecting shafts. This is accomplished by means of an electronic servo system, instead of by a Niemann-type string and band torque amplifier such as is used at the Moore School. The servo mechanism together with changes in the wheel and disk size of the integrator itself make the integrator constant twice as large, and the maximum practical integrator speed double that of the Moore School integrators.

Those of us who have spent a great deal of time and effort in trying to design a servo system for this purpose realize the difficulty of the job, and we want to congratulate Mr. Berry on his solution of the problem.

The differential analyzer servo-system problem is a difficult one because of the wide speed range which must be covered, and also because in the setup of a problem a number of the servo mechanisms may be cascaded; that is, the output of one servo mechanism is the input to the next. A servo system which is stable in itself may not remain stable when interconnected with other similar systems.

The use of a servo system also should make possible, by some rearrangement of the present apparatus, the use of the Kelvin integrator as a differentiating device. This should be a great advantage in the solution of certain differential equations from the standpoint of increasing the accuracy and shortening the solution time.

H. Poritsky (General Electric Company Schenectady, N. Y.): The completion of a new differential analyzer must be regarded as an important occasion in the scientific and engineering world. It is of interest to point out that, while a differential analyzer is primarily a machine for solving ordinary differential equations, it can be adopted for solving simultaneous linear algebraic equations and certain linear partial differential equations.

Let

$$\left.\begin{array}{l} a_{11}x_1+a_{12}x_2+\ldots+a_{1n}x_n=C_1 \\ a_{21}x_1+a_{22}x_2+\ldots \qquad =C_2 \\ \cdot\quad\cdot\quad\cdot\quad\cdot\quad\cdot\quad\cdot\quad\cdot\quad\cdot\quad\cdot \\ a_{n1}x_1+a_{n2}x_2+\ldots \qquad =C_n \end{array}\right\} \quad (1)$$

be a set of *n* linear equations in the variables $x_1, x_2, \ldots, x_n$. The basic idea in solving such a set of equations by means of the differential analyzer is to reduce the equations to the solution of a set of differential equations whose initial values can be found by inspection and whose terminal values yield the desired solution. This may be done in various ways. Thus, if one multiplies all the terms not in the main diagonal by λ

$$\left.\begin{array}{l} a_{11}x_1+\lambda[a_{12}x_2+\ldots+a_{1n}x_n]=C_1 \\ \lambda a_{21}x_1+a_{22}x_2+\lambda[a_{23}x_3+\ldots+a_{2n}x_n]=C_2 \\ \cdot\quad\cdot\quad\cdot\quad\cdot\quad\cdot\quad\cdot\quad\cdot\quad\cdot\quad\cdot \\ \lambda[a_{n1}x_1+\ldots+a_{n,n-1}x_{n-1}]+a_{nn}x_n=C_n \end{array}\right\} \quad (2)$$

then the resulting equations 2 reduce for $\lambda=0$ to the diagonal terms only

$$a_{11}x_1=C_1, a_{22}x_2=C_2, \ldots, a_{nn}x_n=C_n \quad (3)$$

whose solution is found by inspection

$$x_1=C_1/a_{11}, \quad x_2=C_2/a_{22}, \ldots \quad (4)$$

while for $\lambda=1$ they reduce to the original set of equations 1. By differentiating the set of equations 2 with respect to λ, keeping in mind that the variables x_i are functions of λ, one obtains

$$\left.\begin{array}{l} (a_{12}x_2+\ldots+a_{1n}x_n)+a_{11}\dot{x}_1+\lambda(a_{12}\dot{x}_2+ \\ \qquad a_{13}\dot{x}_3+\ldots+a_{1n}\dot{x}_n)=0 \\ (a_{21}x_1+a_{23}x_3+\ldots+a_{2n}x_n)+a_{22}\dot{x}_2+ \\ \qquad \lambda(a_{21}\dot{x}_1+a_{23}\dot{x}_3+\ldots+a_{2n}\dot{x}_n)=0 \\ \cdot\quad\cdot\quad\cdot\quad\cdot\quad\cdot\quad\cdot\quad\cdot\quad\cdot\quad\cdot \\ (a_{n1}x_1+\ldots+a_{n,n-1}x_{n-1})+a_{nn}\dot{x}_n+ \\ \qquad \lambda(a_{n1}\dot{x}_1+\ldots+a_{n,n-1}\dot{x}_{n-1})=0 \end{array}\right\} \quad (5)$$

where dots indicate differentiation with respect to λ. This is a set of differential equations which can be set up on the differential analyzer. One merely starts the differential analyzer for $\lambda=0$ at the initial values determined by equations 3; following the solution to the point where $\lambda=1$, one arrives at the requisite values of the x's which satisfy equations 1.

It is clear, of course, that many other ways of reducing equations 1 to a set of differential equations are possible. Thus one might multiply the right-hand constants of equations 1 by λ. Differentiation then yields

$$\begin{aligned} a_{11}\dot{x}_1+\ldots+a_{1n}\dot{x}_n&=C_1 \\ \cdot\quad\cdot\quad\cdot\quad\cdot\quad&\cdot\quad\cdot\quad\cdot \\ a_{n1}\dot{x}_1+\ldots+a_{nn}\dot{x}_n&=C_n \end{aligned} \tag{6}$$

The solution is started at $x_i=0$ for $\lambda=0$ and run out to $\lambda=1$.

While the solution of linear equations by elimination or by means of determinants is well known, both methods get to be fairly complicated when the number of variables and equations is large. Of course other methods of solving linear equations by machines are known; for instance, in certain cases the network analyzer furnishes one such rather convenient method.

Turning to the question of partial differential equations, one will recall that in an ordinary differential equation the unknown functions depend upon only one independent variable, say a length x, or the time variable t. As already stated, the differential analyzer is a tool for solving such differential equations which express relations between these functions and their rates of growth or derivatives; the answer is given in form of curves exhibiting the variation of these functions with the independent variable. However, many scientific and engineering problems lead to the solution not of ordinary but of *partial* differential equations, where the unknown functions depend upon not one but two or more independent variables. A machine designed for tracing a function of several variables is a decidedly more complicated affair; in trying to design it one gets lost, because the values of the unknown function cannot be arrayed as a simple sequence as in the case of a function of a single independent variable. For this reason it is of interest to point out that, in certain cases, the differential analyzer may be adopted even toward this more difficult problem, provided one is willing to allow certain approximations.

To illustrate with a specific example, consider the well-known Laplace equation

$$\frac{\partial^2 u}{\partial x^2}+\frac{\partial^2 u}{\partial y^2}=0 \tag{7}$$

whose solution is sought over a region R in the (x, y)-plane. It is known that solutions of the differential equation 7 are characterized by the fact that they minimize the integral

$$\int\int\left[\left(\frac{\partial u}{\partial x}\right)^2+\left(\frac{\partial u}{\partial y}\right)^2\right]dxdy \tag{8}$$

over the region R in question among all functions which take on the same boundary values over B, the boundary of R. This being the case, one can dispense with the differential equation 7 and proceed directly to minimize the integral 8, or at least to find among a particular set of functions that function which makes the integral 8 as small as possible. Suppose that for each x the profile of u as a function of y consists of a straight line picked in such a way as to agree with the boundary values, along with a parabola which vanishes at the end points, but whose height is undetermined. One then can choose the height of the parabola in such a way as to minimize the integral 8. When this is done, there results an *ordinary* differential equation for the parameter determining the height of the parabola, and it is the solution of this equation which can be found on the differential analyzer.

The assumed quadratic profile, of course, constitutes an approximation; by forming a third- or fourth-degree profile one is led to a better approximation to the function. This results in a system of ordinary differential equations of higher order. In practice the order would be determined by weighing the accuracy which is desired against the increasing complexity of the resulting system.

After the differential equations have been set up, proper "end-point" conditions at the extreme values of x are required for picking the pertinent solution of the resulting system of differential equations. This may lead to linear algebraic equations whose coefficients are related to particular solutions.

The preceding method is applicable to two-dimensional and axially symmetric problems of fields, both static and alternating-wave type, and to elastic problems. For more details of particular cases see H. Poritsky's paper.[1]

A related and older method of reducing a linear partial differential equation

$$L(u)=f \tag{9}$$

to ordinary ones is due to Galerkin.[2] It consists in multiplying equation 9 by various functions of y, Y_0, Y_1, . . ., and integrating over the proper interval in y, thus replacing equation 9 by

$$\begin{aligned} &\int Y_0L(u)dy=\int Y_0fdy, \\ &\int Y_1L(u)dy=\int Y_1fdy, \ldots \end{aligned} \tag{10}$$

Thus in cases where the partial differential equation is not readily reducible to a minimizing integral, it still may be converted to a system of ordinary differential equations by assuming for each x a shape consisting of several profiles and determining the constants of the latter in such a way as to satisfy the differential equation on the average for that x, and so as to make several moments of the left-handed term of equation 9 equal to the moments of its right-handed member.

A further way of utilizing the differential analyzer in solving differential equations which can be reduced to a minimum integral is by assuming a combination of a particular set of functions, for instance, by means of polynomials of a certain degree in x and y, that is, by a linear combination of a certain number of terms of the display

$$1, x, y, x^2, xy, y^2, \ldots \tag{11}$$

In this case the coefficients of the polynomial are to be determined in such a way as to satisfy the boundary values and so as to render the integral 8 a minimum. In carrying out the latter integration, one of the integrations can be carried out easily, while the other one can be done on the differential analyzer. One then would be led to a set of linear equations in the coefficients, whose solution might be found by means of the network analyzer or the differential analyzer in the manner already pointed out.

RÊFERENCES

1. THE REDUCTION OF THE SOLUTION OF CERTAIN PARTIAL DIFFERENTIAL EQUATIONS TO ORDINARY DIFFERENTIAL EQUATIONS, H. Poritsky. *Proceedings*, Fifth International Congress of Applied Mechanics, 1938, page 700.

2. METHODS IN MECHANICS AND DIFFERENTIAL EQUATIONS, Galerkin. Reports and Memoranda, England Aeronautical Committee, 1798.

Edward W. Kimbark (Massachusetts Institute of Technology, Boston, Mass.; on leave from Northwestern University, Evanston, Ill.): Six years ago, in discussing a paper by Kuehni and Lorraine on "A New A-C Network Analyzer,"[1] I called attention to the possibility of automatically running swing curves, or angle-time curves, in a transient-stability study by means of the combination of a network analyzer (such as the one described then) with integrators of a differential analyzer (such as the one described today). The integrators would eliminate the need of point-by-point solution of the swing equations—now the most time-consuming part of a network-analyzer stability study—as the network analyzer had already eliminated the tedious longhand solution of the electric network.

The network analyzer described on that former occasion had one of the requisites of the proposed scheme: namely, power sources with good voltage regulation, whose voltage magnitude remains constant while the phase varies directly with the mechanical angle. Integrators with suitable servo mechanisms are a second requisite. Now that these two requisites are available in the same laboratory, I wish to call attention again to the proposal and express a hope that the authors may explore the possibilities of speeding up stability studies in this manner.

The principal missing link is a wattmeter provided with a servo mechanism to control the angular position of a high-torque output shaft. This should not be too difficult to develop.

For a more detailed explanation of the proposal, consider the swing equation of a generator

$$\delta=(1/M)\int\int(P_i-P_u)dt^2$$

Here δ is the angular position of the generator with respect to a reference axis rotating at the normal speed of the machine; δ is also the reading of the angle dial of the power source on the network analyzer. M is the inertia constant of the generator, P_i is the mechanical power input to the generator—usually assumed constant, and P_u is the electric-power output of the generator given by the wattmeter on the power source representing that generator. There is such an equation for each machine on the power system, and they are interrelated by the fact that the power output of each machine is a function of the angular positions of all the machines. The network analyzer is used to find the power outputs.

Two integrators are required per machine. (Hence the 14-integrator differential analyzer could solve a seven-machine system.) The disks of all integrators are driven by the time motor. The output of each generator, acting through a proposed servo wattmeter, drives the displacement of the first integrator of the pair; the output of the first integrator displaces the second integrator of the pair. The output of the second integrator represents δ and turns the power source so that its angle dial always reads δ. The output of the second integrator also drives vertically a pencil which at the same time is driven horizontally by the time shaft. This pencil records the swing curve of one machine. These events occur at the same time for all generators on the system. Change gears or an additional integrator are required per generator for adjusting the constant $1/M$.

The initial settings may be made as follows: The network is adjusted for the normal or prefault condition; and the servo mechanisms are then turned on. The displacements of all integrators are set on zero (that is, so that the wheels rest on the centers of the turntables). Next, the fault is applied to the network, causing the power outputs to change and consequently to displace the integrators to the initial values of P_i-P_u. The time motor is then started. After it has turned a number of revolutions representing the clearing time of the fault, the time motor is stopped, and the appropriate network connections are opened to represent clearing of the fault. After the servo wattmeters have been permitted to alter the displacements of the integrators in accordance with the change of outputs caused by clearing the fault, the time motor is restarted and the run resumed. A complete set of swing curves of a multimachine system probably could be obtained in about 15 minutes by this method, as compared to two or three hours by the point-by-point method.

Reference

1. A New A-C Network Analyzer, H. P. Kuehni, R. G. Lorraine. AIEE Transactions, volume 57, 1938, February section, pages 67–73; discussion, July section, pages 419–20.

F. J. Maginniss (General Electric Company, Schenectady, N. Y.): Kuehni and Peterson have given a very complete description of the new General Electric Company differential analyzer. In so doing, they have pointed out the methods by which high accuracy and solution time were obtained. It might be well to group together the factors which lead to improvement in solution time, it being understood that the accuracy to be obtained with either the old or the new design is within the limits required for engineering problems.

Fundamentally the three factors which limit the number of solutions obtainable in a given time are:

1. The maximum scale of the variables which may be used on the integrator lead screw.
2. The maximum allowable turntable speed.
3. The mechanical loads on the integrator outputs.

The first two factors may be combined into one: namely, the maximum level of the variables appearing as shaft rotations in the bed of the machine, that is, the maximum number of revolutions of a shaft per unit of the variable per unit time. A comparison between the new and older design can be obtained by referring to Figure 1a and b of this discussion which show maximum obtainable output shaft speed as a function of integrator setting and as per cent of full-scale setting.

These show two things:

1. The new design allows a larger scale of the variable to appear on the integrator lead screw.
2. When the variable is covering a small range of the lead screw, the new design allows a greater output.

Both of these factors contribute to the speed of solution by allowing a reduction in the required scale of the independent variable. Many problems solved on the differential analyzer consist of transients in which the variables may have very large values for a cycle or two after which they may drop to half or less of their initial peaks. For the remainder of the solution then, only a small percentage of the lead-screw range is being utilized. The availability of high speed at smaller lead-screw ranges is particularly useful for this type problem, especially if the time required for the initial peak transient is only a small percentage of that for the complete solution required.

The load on the output of an integrator may be composed of gear trains, adders, front lash units, input or output tables, and integrator turntable or lead screws or combinations of them. As shown in the body of the paper, two of these loads have been decreased substantially in the new analyzer. The integrator lead-screw load is a light carriage in place of the previous turntable assembly with its associated right-angle gear box. The new adders in addition to being smooth in operation are also a very light load. Reduction in load means that less speed stepdown is required at the integrator outputs with a consequent reduction in solution time.

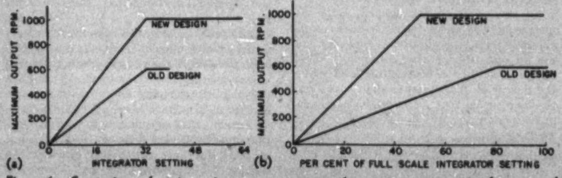


Figure 1. Comparison of maximum integrator output speed versus integrator setting for new- and old-design integrators

H. P. Kuehni and H. A. Peterson: As mentioned by Doctor Weygandt, the problem of cascading several integrators is one of the most difficult from the standpoint of over-all stable performance. We originally had difficulty in cascading several integrators through stiff mechanical connections but have obtained very satisfactory operation by using simple flexible couplings on the integrator-output shafts together with mechanical dampers. Using this auxiliary damping means five integrators have been cascaded. The performance of the last integrator was so smooth that more integrator units undoubtedly can be cascaded when this is required.

Doctor Poritsky has suggested two specific methods whereby the analyzer can be adapted to the solution of special problems. The first of these outlines a procedure for solving linear simultaneous algebraic equations, and the second outlines a procedure for solving partial differential equations. It might be well to point out that the approximate solution of partial differential equations has been accomplished in some cases.[1]

We are aware that there are numerous dynamic systems, including power systems as suggested by Professor Kimbark, studies of which undoubtedly can be facilitated by combining two or more analyzers of different types. Such combinations will materialize as the need for them increases and as such methods prove practical from an economical standpoint.

Reference

1. Mechanical Integration in Electrical Problems, D. R. Hartree. Institution of Electrical Engineers *Journal* (London, England), volume 90, part 1, October 1943, pages 435–42.

Some Aspects of Inductance When Iron Is Present

Discussion of paper 44-45 by L. T. Rader and E. C. Litscher, presented at the AIEE winter technical meeting, New York, N. Y., January 24–28, 1944, and published in AIEE TRANSACTIONS, 1944, March section, pages 133–9.

C. Concordia (General Electric Company Schenectady, N. Y.): This paper is of considerable interest as it calls attention to a situation which may not be generally appreciated. I should like to make some additional points which may supplement the remarks of the authors.

First, I believe there is a great deal to be said for the definition of equation 1 of the paper for general application. This definition comes nearest to the elimination of the concept of inductance and the substitution of the concept of flux linkage which is much more general. It should be pointed out that it is not only engineers concerned with rotating electric machinery who should see the virtues of equation 1, but also those concerned with regulators, contactors, and relays, and, in general, with magnetic devices having moving parts, even if they do not rotate. The advantage of equation 1 over equation 2 appears even without the consideration of saturation, when the inductance (however defined) becomes a function of the varying position of some element of the magnetic circuit.

If saturation is to be considered, it would seem more simple to treat the problem directly in terms of a $\psi-i$ relation rather than to introduce the concept of inductance at all, and, as already mentioned, this is in a sense what equation 1 does. Moreover, when coupled circuits are to be considered, all of these kinds of inductance break down in some respect, and, in fact, problems may be treated simply only when it is possible to identify magnetic circuits and leakage paths in the sense of being able to visualize and calculate the magnetomotive forces acting on the various paths.

Although coupled circuits are not discussed in the paper, eddy currents are, and it may be of interest to mention that the presence of eddy currents may be considered approximately by representing the current path in the solid iron as an equivalent secondary coil, estimating a coupling coefficient from the geometry of the device being considered. It may be important to consider such a coupled circuit, rather than merely to modify the apparent inductance of the coil, in those cases where the flux change as well as the current change are of interest. For example, in the case of a machine field, the effect of the solid iron may be to increase the rate of build-up of the current but to decrease the rate of build-up of the flux.

G. H. Fett (University of Illinois, Urbana, Ill.): The difference between the three definitions of inductance given by equations 1, 2, and 3 can be illustrated clearly by Figure 1 of this discussion. *OF* is the magnetization curve, and *DE* is tangent to the curve at *B*. Then definition 1, $L = N\Phi/I$, gives a value of inductance proportional to the triangular area *OBCO* formed by the straight lines *OB*, *BC*, and *CO*. Definition 2, $L = Nd\Phi/dI$, gives a value of inductance proportional to the triangular area *DCBD*. Definition 3, $L = 2W/I^2$, gives a value of inductance proportional to the area bounded by the magnetization curve and the straight lines *CB* and *OC*.[1]

It is evident from the comparative sizes of the three areas that definition 1 should give the largest value of inductance, and definition 2 the smallest. These conclusions are checked by the authors' Table I.

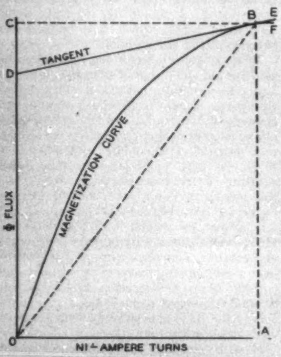


Figure 1. Areas representing inductance

1—$L = N\phi/I$, triangle *OCBO*
2—$L = Nd\phi/dI$, triangle *DCBD*
3—$L = 2J/I^2$, *OCB*—magnetization curve—*O*

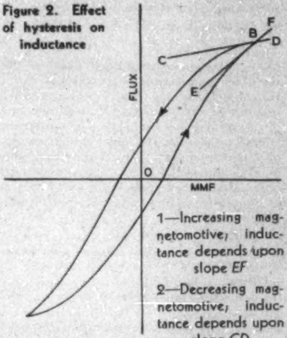


Figure 2. Effect of hysteresis on inductance

1—Increasing magnetomotive; inductance depends upon slope *EF*

2—Decreasing magnetomotive; inductance depends upon slope *CD*

Attention should be called to the anomalous character of inductance when evaluated by definition 2 at a point corresponding to *B* of Figure 2 of this discussion. It is evident that, when hysteresis is present, there are two distinct values of inductance for the same conditions, depending upon the slope of the tangent *EF* on the rising part of the hysteresis loop, and the slope of the tangent *CD* on the decreasing part of the loop. The fact that these two values exist means that the definition given by equation 2 is not of value in this case. Of course, the circuit correctly behaves, because, at the instant that point *B* is reached, dI/dt is zero, and hence any value of *L* will give the zero voltage required.

It should be pointed out that all of the relations developed for inductance have corresponding equations when capacitance is evaluated, even to the fact that the definitions give different values of capacitance when the dielectric permittivity is not constant.

The authors express surprise because L_{ac} is a function of frequency when written in terms of voltage. If the equations in the last column of Table I of the paper are examined, it will be noted that L_{ac} is a function of $(\omega/E)n/2$. Now it is true that (E/ω) is a function of *B*, the flux density. Hence L_{ac} is a function of $(1/B)$, which is expected since a large value of *B* gives a small value of incremental permeability, and a large value of *B* at a definite voltage means a small value of frequency. When, however, L_{ac} is written in terms of the current, the operating point is independent of frequency, as shown in Table I.

When the destruction of contacts in an inductive circuit is studied, attention should be directed toward the energy-stored definition of inductance. However, the specification must be made that the value is obtained from the decreasing hysteresis curve when the contacts are opened, since the increasing hysteresis curve includes some energy which is dissipated in the core and not in the arc at the contacts. When studying welding of the contacts on make, the definition 2 is important in specifying the limiting current. Hence the particular problem dictates which definition is to be used.

REFERENCE

1. INTRODUCTION TO CIRCUIT ANALYSIS (book). A. R. Knight, G. H. Fett. Harper and Brothers, 1943. Chapter 3.

Walther Richter (Allis-Chalmers Manufacturing Company, Milwaukee, Wis.): This paper is of a type of which more decidedly would be welcome, and the authors should be congratulated on the excellent presentation of the subject. The publications of the Institute, of course, would fail in their duty if they did not present papers on developments in specialized fields such as circuit breakers or electric machinery, but a paper like this one treats fundamental concepts of importance to all electrical engineers, no matter what their specialized field may be. It can be stated without fear of contradiction that all of us could stand more knowledge of fundamentals.

The authors show in a very clear way that not less than seven definitions are possible for a value which most of us are using every day without giving too much thought to the complexity of the problem. All seven definitions will give numerically different results, but for one particular problem, only one of the definitions is the right one to use! This is something to think about.

Table I shows the inductance L_{ac} to be a function of frequency when expressed in terms of the applied voltage, a result which the authors consider as somewhat surprising. Equation 35 in Appendix V shows, however, that with E_m held constant, the value B_o will be inversely proportional to the frequency; expressed in words, this states that with a sinusoidal voltage of constant amplitude but varying frequency applied to an inductance, the maximum flux will be inversely proportional to the frequency. With a nonlinear relation between flux and exciting current, the operating point will therefore cover different parts of the *B-H* curve, and the frequency dependence of the inductance therefore, is easily explained. If the inductance is expressed in terms of current, the operating point always will cover the same part of the *B-H* curve, regardless of frequency, and the inductance will be independent of frequency.

Among the conclusions shown in the paper, number 7 brings up a question of much interest to the designers of circuit-interrupting equipment. The problem of standardizing circuit-breaker tests is considerably more complicated than one would think. In order to simulate the conditions of a stalled induction motor for instance, the use of an air-core reactor with a certain series resistance of such value that the same current would flow as in actual motor circuit has been proposed. Such a combination imposes, however, a considerably higher strain on the contacts than the actual motor circuit where iron is present in the magnetic circuit. Furthermore, it should be kept in mind that whereas, for any particular frequency, any series combination of resistance and inductance can be replaced by a parallel combination, the transients produced when opening a series circuit are considerably more severe than the transients in the parallel circuit. In a magnetic circuit containing iron, the eddy currents produced when opening the electric circuit have a considerable damping effect which reduces the induced voltage across the contacts in the moment of opening. It would seem that it will be very difficult to standardize on an inductance containing iron for the testing of circuit breakers, and the logical solution would be an air-core reactor with a resistance shunted across it.

Design Features of Generators for Diesel-Electric Switcher Locomotives

Discussion and author's closure of paper 44-25 by C. A. Atwell, presented at the AIEE winter technical meeting, New York, N. Y., January 24-28, 1944, and published in AIEE TRANSACTIONS, 1944, February section, pages 41-5.

John C. Aydelott (General Electric Company, Erie, Pa.): Since the simple generator-excitation scheme described was used first by General Electric Company on gas-electric busses and was applied subsequently to industrial Diesel-electric locomotives, in all fairness it should be noted that considerable engine stalling is involved, causing considerable loss of horsepower when the generator field is cold as it often will be in slow-speed-switching service. A more complicated scheme may be justified to avoid engine stalling and to insure full engine output whenever it is called for. Such a refined scheme long has been the goal of the designers of Diesel-electric-locomotive transmission apparatus. An accurate, reliable, and economical solution is much nearer to being a reality today than at any time in the past, thanks to the high amplification and quick response characteristics of the amplidyne exciter.

C. A. Atwell: The use of a self-excited d-c generator with a small amount of separate excitation from a battery for loading an engine is not new.

In 1924 the Westinghouse company received an order for a generator for use on a large eight-wheel gas-electric bus for the Versare Corporation. At that time it was decided to use a self-excited 200-volt generator with some separate excitation from a 12-volt battery. In that application there was a small handle on the steering-wheel column that made several changes of resistance in the self-excited field circuit.

The results with generators using this type of excitation were not very satisfactory with regard to engine speed regulation, however, until several years ago when we started modifying the load-saturation curves by special one-direction shaping of the main pole faces. As the generator voltage is necessarily low at high-armature amperes, the armature reaction is too high at maximum loads. By correct pole-face shaping, the effect of armature reaction can be made just the right amount at maximum load. The net result is a much "flatter" engine-generator speed regulation over a given range of ampere load.

The expression "engine stalling," that Mr. Aydelott uses in his discussion, is probably misleading to most readers. Performance, such as that shown in Figure 6 of the paper, does not have any tendency to stall the engine. Over certain ranges of the ampere load, the generator torque is greater than the engine torque, causing the speed to drop a maximum of about ten per cent. A maximum speed drop of 15 per cent in no way should be harmful to the engine. While there is some loss in engine-horsepower output caused by operating at the lower speeds, this decreased horsepower does not reduce the pulling ability of the locomotive, but only reduces the speed at which the pulling is done.

The effect of change in excitation between a "cold" and "hot" field is reduced to a small amount by designing the field for low-temperature rise and also by using a large percentage of external resistance.

The fact that a large percentage of small- and medium-size switcher operation is done at less than full throttle (and, consequently, less than full horsepower and locomotive speed) is proof that 15 per cent speed reduction at some full-throttle loads scarcely would be noticed in the day's work. At least it does not justify the use of a complicated scheme for obtaining an exactly flat speed regulation at full throttle.

Special field-excitation schemes for obtaining exact engine loading are justified on the larger Diesel-electric locomotives (either freight or passenger) that operate for long periods at near maximum horsepower.

A scheme that is as near the ultimate as has been used so far is described in the article given as the first reference at the close of the paper.

Electrification of Large Open-Pit Copper Mines

(44-20)

Modern Electric Locomotives in Open-Pit Mine Haulage

(44-28)

Discussion of paper 44-20 by L. W. Birch, and discussion and author's closure of paper 44-28 by J. E. Borland and L. G. Riley, presented at the AIEE winter technical meeting, New York, N. Y., January 24-28, 1944, and published in AIEE TRANSACTIONS, 1944, January and February sections, pages 30-8 and 52-8.

Sidney Withington (New York, New Haven, and Hartford Railroad Company, New Haven, Conn.): These two papers contain data of value in connection with a special type of railroad operation, and supplement each other very well. It would add to the interest of both papers if some mention could be made of the method used in dividing the service between electric locomotives and the Diesel equipment in operation at Morenci. It would be logical to use the Diesel locomotives where it is necessary to move the tracks and contact system frequently and where blasting debris would injure the contact system; leaving the electric equipment to operate where the power distribution can be permanently installed.

It may be asking too much under present conditions, but it would be of great interest, also, to learn something of the relative cost of maintenance and operation of the electric and Diesel locomotives in this service, where the locomotives of each type presumably are maintained in the same shops. The cost of operation appropriately could be correlated with the unit cost of electric power and the cost of Diesel fuel oil in that locality.

L. G. Riley (nonmember): The division of service between electric and Diesel locomotives is determined basically by the fact that the main-line heavy-grade haulage to the reduction plant involves starting and speed characteristics beyond the capabilities of the Diesel locomotives available on this property. I quote from the last page of the article: "Diesel locomotives (are) used in waste haulage and for general duty." The latter includes moving of cranes and track sections, delivery of drilling supplies, and miscellaneous service haulage.

No data have been compiled by the authors on the relative cost of maintenance and operation of the electric and Diesel locomotives. Any direct comparison might be inconclusive because of dissimilarities in the service.

Gear Ratio and Its Effect on Traction Motors

Discussion of paper 44-26 by G. M. Woods, presented at the AIEE winter technical meeting, New York, N. Y., January 24-28, 1944, and published in AIEE TRANSACTIONS, 1944, February section, pages 65-8.

John C. Aydelott (General Electric Company, Erie, Pa.): At a particular state of the art, and with a given arrangement of gearing, gear reduction can be increased only to a point which in railway service we know as "maximum reduction," at which the pinion is the smallest considered to be reliable. Most railway designs are worked out on the basis of maximum reduction. By changing from single reduction to double reduction or from double reduction to triple reduction, the designer has a whole new range of possibilities involving, of course, increased complication and more expensive gearing. In some cases the best solution is to use single-reduction gearing and to put enough material in the motor to do the job. In other cases the best solution is to use double-reduction gearing and much less motor material. There is a range in which these two solutions tend to overlap to some extent. As a rule of thumb, it may be stated that double reduction is particularly useful at speeds below 35 miles per hour, and that above 35 miles per hour single reduction generally will be found preferable. As indicated by Mr. Woods, however, the trend in railway motor design is still toward higher and higher speeds.

The MHO-Carrier Relaying Scheme

Discussion and author's closure of paper 44-46 by R. E. Cordray and A. R. van C. Warrington, presented at the AIEE winter technical meeting, New York, N. Y., January 24–28, 1944, and published in AIEE TRANSACTIONS, May section, pages 228–35.

W. R. Brownlee (Commonwealth and Southern Corporation, Jackson, Mich.): Wartime conditions have accelerated studies of methods of securing maximum emergency capability from transmission lines. It has been practicable to build very few new lines to accompany increased loads and additional generating equipment, and the necessary pooling of power resources, including co-ordination of generating-plant maintenance schedules over wide areas, has greatly increased the duties on these lines and interconnections. It is hoped that the economy benefits of peacetime will be attractive enough to maintain an urgent desire for improved reliability of transmission connections. The emergency reliability of lines is especially important, since the avoidable opening of one or more unfaulted transmission lines along with the faulted line may result in complete separation at all other points (through instability) of entire generating plants from loads, overspeed tripping of turbine throttles, loss of station service power, and prolonged service interruptions to widespread areas.

The avoidable tripping of transmission lines is more frequent than is generally recognized. Sometimes the relay operations are blamed on highly improbable simultaneous faults, or dismissed as "out-of-step operations," with no critical analysis to show whether the system actually lost synchronism or whether swing currents operated the relays incorrectly. Even when causes are analyzed and the operations found preventable, the relatively high cost of carrier-current relaying and its formidable complexity have made its consideration a last desperate resort. The authors are to be commended, therefore, on the simplification which they have made in the basic carrier relay equipment and its functions.

The simplification of system swing characteristics into a straight line at right angles to the circuit impedance and at an elevation depending on the distance of the relay terminal from the system electrical center is most useful for general analysis. An interesting deviation from this characteristic has been found in connection with an a-c calculating-board stability study, which included readings of current, voltage, and phase angle at 0.1-second intervals at the principal tie-line terminals. This characteristic was neither a line nor a circle but a badly distorted version of both, with the relative height on the circuit-impedance line changing during the power swing. The effective relay impedance was found to enter and leave certain relay protection zones twice during one major-swing cycle. In practice, swing current on a line is usually initiated by a fault on another line at a point near their common bus. If the faulted line is protected with distance relays, the swing will progress after instantaneous clearing at the near end, but the supplying of fault current to the remote end of the remaining network changes the value of equivalent reactance and therefore the location of the electrical center. This is altered when the far end of the faulted line is cleared. On this particular system it was found necessary to represent five separate generators on the system connected to one terminal of the tie line and two at the other. Swings between units connected by the shorter lines have time periods about one quarter of that of the major swing, and these so affect the equivalent system reactance as to produce marked variations in the effective impedance presented to the relays. This analysis suggests the desirability of including swing currents and voltages in stability studies, and also points to another difficulty which besets any blocking scheme depending solely on the rate of change of relay impedance.

Answers to the following questions are desired:

1. What is the over-all tripping speed of the arrangement described?
2. Are standard bushing potential devices (for 138-kv lines, for example) entirely adequate for the operation of the scheme?
3. The authors' conclusions pronounce the protective scheme as applicable to all lines which are long enough to justify the cost of carrier-current relaying. Will they please state this minimum length (on a 138-kv-line basis), and thus reveal not only the application limit but also their judgment regarding how short the line must be before the cost of carrier-current relay equipment is not justified?

S. L. Goldsborough (Westinghouse Electric and Manufacturing Company, Newark, N. J.): This paper is an excellent redescription of material presented to the AIEE in January 1943. The intimation in the paper is that this is a new carrier relay scheme, but a close examination of the material given does not disclose anything particularly new, as all of the relay elements and the method of using them have been in use for a number of years. Also, the statement is made that this so-called new scheme is a simplification. Whatever simplification has been obtained in the over-all scheme apparently is done merely by omitting the backup elements or elements that have been used in the carrier scheme for a number of years but which had no direct function in the carrier scheme itself. Some simplification has been made in the ground relaying in that ground preference has been omitted and, also, the overcurrent ground relays are missing. This means a certain degree of simplification but, as will be pointed out later, the omission of the overcurrent ground elements introduces some definite hazards in the scheme.

It is felt that the designation of the offset impedance characteristic in the voltage restrained directional element is confusing to the industry. The industry has been accustomed to considering a relay that measures ohms as an ohm relay, and I feel that the designation of a particular kind of ohm relay as a MHO relay is confusing. Using the definition of a MHO relay that is implied in the paper, that is, a mho element that trips when the mhos are greater than a critical value, then any "under" ohm relay also can be termed an "over" mho relay. On this basis there is no more justification for calling the voltage restrained directional element a MHO relay than there is calling any other ohm-measuring relay a MHO relay.

The paper exhibits considerable confusion in regard to this so-called MHO relay which is typified in Figure 6A. This figure is obviously an error, since the voltage restraint does not and cannot bend the straight-line characteristic of a directional element on a current diagram into the circles as stated, and neither can the circles, as shown, be converted into the circular mho characteristic on the R and X diagram.

As previously mentioned, the carrier scheme in respect to the ground relays apparently has been simplified somewhat. However, this simplification apparently has been made at the expense of correct operation under certain very likely system conditions. The use of directional elements alone to start and remove carrier very greatly increases the sensitivity and therefore raises the doubt that for external ground faults the carrier-starting directional element will function before the carrier-removal directional element functions. A specific instance of this would be the case of fairly short lines where it is necessary to use voltage polarization. In this case the current through the directional elements at both ends of the line will be the same and the polarizing voltages also will be practically the same. Therefore, when the fault current hovers around the pickup value of the directional elements, there is no definite assurance that the directional element closest to the fault will provide a blocking signal before the carrier-removing directional element at the other end of the line functions.

The authors claim that the directional element cannot operate incorrectly on series ground currents because of current-transformer unbalances, since only one coil is affected. It is agreed that this is the case if consideration is given only to effect of secondary current unbalances. When, however, the possibility of primary-circuit unbalances is considered, the scheme proposed definitely will give incorrect results. In many cases the phase conductors of the transmission line are not transposed. This means that on phase-to-phase faults, ground current will be induced in the third phase. This induced current will flow from the line into the bus and down the grounding-bank neutral at one end of the line, and will flow up the neutral from the bus and into the line at the other terminal. Under this condition the ground tripping directional elements at both ends of the line will close contacts and thus incorrectly trip for an external phase-to-phase fault. This condition has been met with many times on actual power systems, but heretofore it has been overcome by the use of separate starter and tripping elements with different sensitivities. It is very much doubted, therefore, that the scheme can be simplified in this manner with impunity.

E. E. George (Southwest Power Pool, Little Rock, Ark.): The new scheme appears to have several outstanding advantages, even over the present Kansas–Nebraska tie-line scheme which has been operating satisfactorily. Of course, the new scheme should have thorough staged testing when installed. The elimination of phase or ground preference is a step forward. In this area, out-of-step blocking and back up protection of conventional schemes have had to be reduced or omitted on account of heavy line loading; straightforward use of directional ground relays of the current polarized type is also desirable. The power equation 1 and impedance equation 3 in the previous Ebasco paper[1] can be related as follows: $\phi=180-2\theta-\delta_{rs}$.

Reference

1. Performance Requirements for Relays on Unusually Long Transmission Lines, F. C. Poage, C. H. Streifus, D. M. MacGregor, E. E. George. AIEE Transactions, volume 62, 1943, June section, pages 275–83.

C. A. Streifus (Ebasco Services, Incorporated, New York, N. Y.): The MHO-carrier relaying system described is considerably simpler, with fewer elements and contacts than other carrier systems that are in the long-line class. It appears to be satisfactory for shorter transmission lines, and at the same time its basic principles embody those of long-line relaying, permitting it, to a great extent, to meet rather exacting specifications. However, each long-line relaying problem and application is a special one and, therefore, each should be carefully analyzed. Size of systems on each end of line; magnitude of system impedance-to-line terminal busses; and maximum permissible angular difference between system internal transient voltages at which systems may be expected to recover are related to relaying problems and to maximum permissible angular difference of line terminal voltages, further emphasizing importance of detailed analysis of each problem to see that the proposed relays will meet all conditions.

While the simplified method of calculating "impedance circle diagrams," as discussed under "Impedance Diagrams" in the paper, may be satisfactory for short lines, exact calculations should be used for lines of 200 to 300 miles or longer, particularly if the combinations of line and terminal systems can tolerate a comparatively high angular difference between transient internal voltages and still recover. Location of Z as viewed by terminal relays on the impedance diagram for maximum permissible swing and magnitude of equivalent impedance of peak load may influence the number of elements required, such as addition of angle impedance units and setting of carrier-start unit, and, therefore, more exact methods should be employed for long lines.

Figures 2, 3A, and 12 in the paper show values of θ, the angle between *system transient internal voltages*. Since the impedance circle diagrams, or the straight line in the previously mentioned figures representing power-swing impedance loci, are based on a definite relation between line $ABCD$ constants, V_A and V_B, and δ, representing angular difference between *line terminal voltages*, as emphasized in references 2 and 3 of the paper, it would seem more correct and desirable to have values of δ shown on these figures rather than θ, a value describing internal system conditions. Perhaps both could be shown.

A. R. van C. Warrington: We were extremely gratified by the number of excellent discussions of our paper.

The answers to Mr. Brownlee's interesting questions follow.

1. The over-all tripping time of the arrangement described is substantially the same as that of the standard one-cycle *GCX* distance relay. The same units are employed except that the MHO unit, which is the starting unit of the standard distance relay, is set for less reach.

The tendency to slower operation of the MHO unit for faults near the end of the section resulting from the lower ohmic setting is offset by the powerful polarizing circuit, which has three times the ampere turns of the standard *GCX* relay.

A single MHO unit used as zone 1 without carrier may operate slightly faster than the *GCX* relay. In addition, it is an excellent backup device for carrier or pilot-wire applications because it is a simple self-contained directional distance relay and presents only a single set of contacts between the battery and the trip coil.

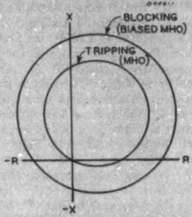


Figure 1. Characteristic of blocking relay for providing out-of-step blocking with a MHO unit

Blocking relay is a MHO unit with current bias KI^2 and has the general equation

$$KI^2-K^2E'+EI\cos(\phi-\theta)=0$$

2. The burden imposed by the potential circuits is about the same as that of the standard *GCX* relay. The internal phase-angle conditions of the MHO unit enable more watts to be put in the coils with no increase in watts input to the circuit because there is much less loss in series resistors. Bushing potential devices of 138 kilovolts have enough capacity to supply the relay.

3. The length of line to which it is economical to apply carrier relaying depends upon a comparison of the installed cost of relay equipment and the over-all cost of the equipment and line to be protected. Generally the value of the relay protection, communication, and other services derived from the equipment, should easily justify the slight increase in over-all project investment for the carrier relay equipment.

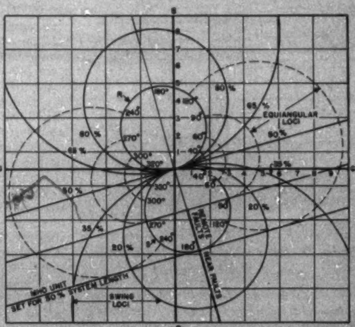
However, since a direct question deserves a direct answer, it is our observation that, for lines longer than 15 miles, carrier is generally less expensive than pilot-wire relaying with leased wires.

A definite contribution the manufacturer can make to this question is a statement of the minimum length of line to which the ohm unit is applicable. If 300/5-ampere current transformers are assumed, the minimum length of line that could be protected with the MHO unit is at present 25 miles at 138 kilovolts, with a minimum fault of 300 amperes, which corresponds to a one-ohm secondary. Below this limit the current pickup is above normal current, and will increase as the ohmic setting is reduced further. In other words there is a minimum voltage limit for a fault at the end of the protected section.

Mr. Goldsborough's comments will be discussed now. As he points out, we have already had many years of experience with the relay elements used in the MHO carrier, but I think he has overlooked the fact that the new method of setting and connecting the units has resulted in much improved protection with a simpler circuit because of the incorporation of characteristics in the distance units which formerly had to be obtained by additional relays. The main changes are:

1. The use of a MHO unit for limiting the reach of zone 1, zone 2, and carrier trip, instead of the conventional reactance or impedance units.
2. The use of a nondirectional impedance unit for zone 3 reaching back one section of the line, instead of a directional impedance or starting unit reaching forward two sections.
3. The use of an offset MHO unit concentric with the MHO unit, instead of the conventional impedance unit for out-of-step indication.

MHO units of this type, including even the memory action, have been used successfully since 1937 in General Electric Company relays by the Pennsylvania Railroad



The solid circles represent loci of admittance vectors presented to the relays at the sending end S, at the receiving end, R, and at intermediate points 20, 35, 50, 65, and 80 per cent of the system impedance from the sending end. Refer to Figure 2 of the paper for legend

The abscissas are conductance, G; the ordinates are susceptance, B

The units correspond to the reciprocals of the values in Figure 2 of the paper; five units correspond to 50 per cent system impedance; 2.5 units correspond to 100 per cent system impedance

Figure 2. Admittance diagram of a 75-degree system

for the same purpose as the present units—for distinguishing between fault and load impedance of equal magnitude.

LIMITATIONS OF OLDER CARRIER SCHEMES

Originally, a power swing was thought to act like a traveling three-phase fault which passed through the electrical center when the generating sources were 180 degrees out of phase. Whereas this is still true, yet it is misleading, because the traveling three-phase fault does not enter into the fault zone along the line LL' in Figure 3A of the paper, but enters along a line at right angles to this line. This definitely limits the load-carrying ability of the transmission line to the point where present-day relays are unsatisfactory. For example, the starting-unit reach must not be set for more than half the total impedance between the equivalent generators of the system, and similar limitations exist for the impedance relay.

Probably the principal objection to present-day carrier-current schemes is their complexity. Relays have been added repeatedly to take care of conditions as they came up, both real and imaginary, until now the elementary diagram is unduly complicated. Since an out-of-step condition is a three-phase phenomenon, the contacts of three impedance-type fault detectors were connected in series to control a time-delay auxiliary relay which would open the trip circuit if not prevented by the contacts of other fault-detector relays which had higher current settings and instantaneous operation. It was soon found that the blocking might not be set up on a swing started by a phase-to-phase fault because the combined swing and fault current was liable to be so unbalanced that at least one fault detector would fail to pickup. This was remedied by connecting the initiating fault detectors in parallel, which has the disadvantage of delaying tripping of an internal fault immediately following an external fault behind the bus.

In addition to the inability to distinguish between power swings and out-of-step conditions, another defect was the inability to trip on an internal phase fault occurring during a swing. The earlier carrier relays included an instantaneous auxiliary relay which would cut off the out-of-step blocking as soon as any fault detector reset; this allowed tripping somewhat before the zero current point of the swing, but was abandoned because it permitted wrong tripping by the distance relays which could overreach when measuring the swing impedance in parallel with the fault impedance.

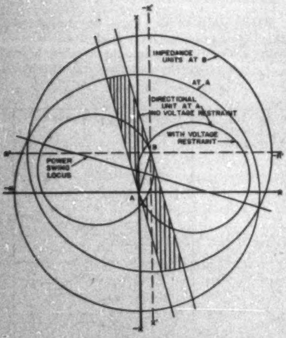


Figure 3. GMB relay characteristics plotted on an R and X diagram

The tripping area is the vertically shaded portion plus the unshaded center. The latter is the tripping area when voltage restraint is not removed

The new scheme has none of these defects because it does not use the rate-of-change-of-impedance method of blocking on swings. It measures the impedance of the system at all times, and does not need blocking because its pickup on load and swing currents is high enough to prevent tripping except on out-of-step conditions.

Mr. Goldsborough apparently failed to notice the paragraph headed "Origin of the Term MHO Unit," which explains why a relay which measures a constant component of admittance is a MHO unit, and not an ohm unit. Mathematically the MHO unit measures $Y\cos(\phi-\theta)$ in mhos or $Z/\cos(\phi-\theta)$ in ohms. The first expression is a constant component of admittance at the angle θ; the second has no constant component of impedance at any angle.

He will be less confused by Figure 6A of the paper if he regards it as an impedance diagram. His oral statement that the memory action varies the pickup of the relay was based on the assumption that the memory action affects only the operating torque, whereas actually it affects the restraining torque equally and hence has no effect on the pickup. To illustrate this mathematically, the torque of the MHO unit can be written $E_p(I\cos(\phi-\theta)-E_r)$, where E_p and E_r are the polarizing and restraining potentials, ϕ is the impedance angle of the line, and θ is the angle of the MHO-unit characteristic.

He stated that a MHO unit having only E^2 and EI torques would be sluggish during nearby faults because E tends to zero. Actually this is not so, because the memory action of the MHO unit is so strong that the operating time with a 20-ampere fault is the same at zero voltage as at 50 per cent of normal.

His belief that the lack of an overcurrent ground unit will cause wrong tripping on an external fault is based on the erroneous assumption that the G_2 is as sensitive as GD_1. In the case he cites, where the residual current and potential are the same at both ends of the section, GD_1 at the blocking end would have at least 50 per cent more torque than GD_2 at the tripping end.

In the second case of possible ground relay misoperation we presume that the induced current comes from a parallel line and that the polarization is by neutral current transformers. In such a case the GD_2 unit can be given the same pickup as the G_2 unit which was eliminated.

The current bias he proposes for making the beam unit work as a MHO unit is used in the MHO carrier for recognizing out-of-step conditions where it is desired not to reclose after tripping on out-of-step conditions. With the current bias, the blocking-unit characteristic is made concentric with the MHO unit tripping characteristic but of larger radius so that any swing condition will operate the blocking relay first, as shown in Figure 1 of the discussion.

The torque equation of the MHO unit at the threshold of operation is $EI\cos(\phi-\theta)-K'E^2=0$. The current bias necessary to produce this characteristic adds an I^2 term to the equation of the MHO unit so that it now becomes the general equation $KI^2-K'E^2+EI\cos(\phi-\theta)=0$, which is a circle whose radius is $\sqrt{\frac{1+4KK'}{2K'}}$ and whose center is $1/2K'$ from the origin at an angle θ from the reference (R) axis. This subject is covered more thoroughly in "A Condensation of the Theory of Relays" by A. R. van C. Warrington.[1]

We have been asked for characteristics of our *GMB* carrier relay similar to Figures 4A and 4B of the paper. This is given in Figure 3 of the discussion and shows some interesting properties. It is the first use of a polyphase MHO unit, which was accomplished in 1937 by not removing voltage restraint by the impedance fault detectors. The tripping area is then defined by the area between the two MHO-unit circles. The angle of maximum torque is 20 degrees, and was chosen when the *GMB* was first designed as being better for out-of-step conditions, without clearly knowing why. For phase-to-phase faults the angle of maximum torque shifts to 50 degrees and the reach in ohms is halved—the main disadvantage of a polyphase MHO unit.

Note that the tripping area is so limited in the direction of power swing that no blinders would be needed, even for the longest lines. Casual inspection would indicate no margin in the mho characteristics for operation on arcs, but it must be remembered that the arcs are reversed for the relay at B so that arc resistance is to the left for that relay and to the right for the relay at A.

Some engineers asked why we did not plot the line and relay characteristics on an admittance diagram, since we are dealing with a MHO unit. This suggestion would please those individuals who are used to thinking more in terms of current pickup values of relays. An admittance diagram is shown in Figure 2 of the discussion which corresponds to the impedance diagram that was Figure 2 in the paper.

Zero load is at the origin; as the load increases it progresses along one of the circles marked power-swing loci. A power swing may oscillate through the origin to points on each side such as 60 degrees on one side to 300 degrees on the other side. The smallest swing circles are those of the sending- and receiving-end terminals, and it is impossible, of course, to have a condition inside these circles. The MHO-unit characteristic is a straight line at right angles to the fault line, and it can be seen that a typical MHO-unit characteristic will operate on power swings up to say 120 degrees of system separation.

As Mr. Streifus points out, the simplified method illustrated in Figure 2 of the paper for estimating the effect of power swings is satisfactory for ordinary lines, but the exact method is necessary for lines over 200 miles long, particularly if the system can swing to a large angle of separation and still recover. The same comment, of course, applies to the admittance diagram in this discussion.

REFERENCE

1. A CONDENSATION OF THE THEORY OF RELAYS, A. R. van C. Warrington. *General Electric Review*, volume 43, September 1940, pages 370–3.

The Functions of Ground Preference in Carrier-Current Relay Schemes

Discussion of paper 44-51 by S. C. Leyland and S. L. Goldsborough, presented at the AIEE winter technical meeting, New York, N. Y., January 24–28, 1944, and published in AIEE TRANSACTIONS, 1944, March section, pages 97–100.

A. R. van C. Warrington (General Electric Company, Philadelphia, Pa.): In addition to the ground preference troubles cited by the authors, an external ground fault has to be cleared before a simultaneous internal fault can be cleared. Furthermore, spurious residual current caused by current-transformer inaccuracy during a heavy internal phase fault can delay or prevent tripping.

The authors have found that the use of delta current in phase directional and phase impedance relays reduces the likelihood of wrong operation on heavy loads or on zero-sequence currents unaccompanied by positive- and negative-sequence currents.

This principle has been used in the MHO relay described in the paper by Cordray and Warrington.[1] In the MHO relay the possibility of such wrong operation is further reduced by having maximum torque at a lagging angle and, also, by the fact that the relay has both directional and impedance characteristics, so that both have to be satisfied at the same time to operate the relay.

With these desirable characteristics it was possible to eliminate both phase and ground preference, which not only avoided their attendant troubles but, as will be seen in Figure 8 of the Cordray–Warrington paper,[1] simplified the circuit.

REFERENCE

1. THE MHO-CARRIER RELAYING SCHEME, R. E. Cordray, A. R. van C. Warrington. AIEE TRANSACTIONS, volume 63, 1944, May section, pages 228–35.

Oliver Ramsaur (nonmember; Pennsylvania Power and Light Company, Allentown, Pa.): With this paper, the authors fill a long-felt need for a comprehensive review of the reasons for and behavior of ground preference in carrier-current relay schemes. With several schemes to choose from, and with their operation so intimately associated with so many fault conditions and other relay functions, up to now one has been in a quandary as to which scheme (if any at all) best fits the needs of a particular system. This paper goes far toward clearing up the haze, particularly in regard to relay performance during the interline fault on the system in Figure 5 of the paper.

It should be noted that the removal of the ground overcurrent element from the carrier-stop circuit to prevent "in-between" operation for two-phase-to-ground faults is not a substitution of some other form of preference for ground preference, as one might infer at first reading. It is still ground preference, but of a different form, in that carrier is stopped by operation of the ground directional element alone instead of the more usual combination of both the ground directional element and ground overcurrent tripping element. Another scheme being used extensively is that of using the ground directional and ground overcurrent *starting* elements to stop carrier. This avoids the possibility mentioned by the authors, that a very sensitive ground directional element may operate incorrectly on spurious currents; and at the same time it eliminates the "in-between" operation, because the same element is used for both starting and stopping carrier (subject to further control by the directional element, of course). In either case, of course, advantage is taken of the fact that carrier transmission at a given terminal is needed only when fault current flows out of the line section.

Another specific instance where phase preference has been used successfully for a different and special purpose may be of interest. Tapped solidly off a tie line protected by carrier-pilot relays and equipped for one automatic rapid reclosure is a large delta–delta step-down transformer supplying a stub load, almost entirely nonsynchronous. This terminal is equipped with carrier phase impedance and transformer differential relays (ground relays are not required because no zero-sequence current can be supplied by or transmitted through the transformer) primarily to avoid overtripping of the other two terminals for heavy faults beyond the oil circuit breaker on the low-tension side of the transformer, and to provide sensitive differential protection of the transformer. In addition, the transformer breaker is tripped by the operation of all of three instantaneous undervoltage relays in order to disconnect the load prior to reclosure following a line tripout. But these undervoltage relays, unhampered, would also trip for a nearby three-phase fault on one of the distribution feeders, so the carrier phase relays are given preference over these in the preference circuit. Thus, for a feeder fault, carrier is started and held on by the phase relays; and the undervoltage relays can neither stop carrier nor trip the main breaker. The preference scheme undoubtedly can be used to good advantage for other such special purposes.

R. C. Ericson (Northern Indiana Public Service Company, Hammond, Ind.): The authors should be complimented on their able presentation of the subject matters contained in the paper.

The fact that the paper is confined to ground preference as obtained only by the use of relay contacts and neglects the method where the ground directional element and the polyphase directional element are assembled on a common shaft somewhat limits the paper's value from the operating engineer's standpoint. Adequate discussion of this latter method, however, very likely would offer sufficient material for a separate paper.

We should like to call to the authors' attention the possibility of improper operation of ground-preference schemes when applied to very short line sections with unbalanced phase impedances. Where the system short-circuit currents are heavy and their magnitude and phase relation are determined by impedances (generally balanced) outside of the short line section, it is possible for three-phase short circuits beyond the short section to develop enough zero-phase-sequence voltage within the short section to cause sensitive set ground relays to operate in an undesirable manner.

The writer has seen at least one case in his experience where this was apparently the cause of an improper operation. The particular installations involved use a three-unit combination (phase and ground-directional and voltage-restraint elements all on one shaft) for fault detection and preference as well as directional indication. The normal type of connection with this scheme is to remove the voltage restraint on the occurrence of both phase and ground faults and also to remove the potential supply to the phase directional unit upon the occurrence of a ground fault. This removal of potential supply is accomplished by impedance phase-fault detectors and a residual-current fault detector.

In this particular case, after the improper operations, the connections were changed so that only the voltage restraint is removed by the residual relay. Dependence is then placed on the excess torque of the ground directional element to give proper ground preference. Calculations show that in this particular instance future operation should be satisfactory.

Since lack of adequate line transpositions is one of the greatest sources of asymmetry in overhead transmission lines, it may be well to co-ordinate closely the line design with the ground relaying design on certain types of systems.

A Versatile Power-Line-Carrier System

Discussion and authors' closure of paper 44-34 by H. W. Lensner and J. B. Singel, presented at the AIEE winter technical meeting, New York, N. Y., January 24–28, 1944, and published in AIEE TRANSACTIONS, 1944, March section, pages 129–33.

F. M. Rives (General Electric Company, Schenectady, N. Y.): The general idea of providing several simultaneous telegraph or signal channels by modulating a single carrier with several closely spaced audio tone frequencies has been used for many years in wire-line telegraph practice. Its first use in power-line carrier practice dates back only a few years; one of the first installations involving modern equipment of this type was described in a paper presented at the 1942 AIEE winter technical meeting.[1] However, the numerous advantages of multiple-channel audio-tone systems, particularly in providing additional joint-use or simultaneous functions, have not received adequate treatment previously, and the authors of this paper are to be commended for their contribution to this subject.

While it is realized that only a limited amount of detail can be included in the general treatment of a broad subject of this nature, it would seem desirable to differentiate between various types of multiple-use applications, since this consideration has an important bearing on the choice of equipment best suited for a particular problem.

First, it is necessary to know the total number of functions a given equipment or channel is required to handle. Next to be established is the type of signal to be transmitted for each of the functions, that is, whether it is a narrow-band impulse-type signal or a wide-band voice-frequency signal. Also at this point, the degree of reliability, or freedom from interference, or accidental operation required for each of the functions should be established. Finally, it should be known whether all of the several functions require the channel simultaneously or whether some of them can be set up on a preferential or joint-use basis. In connection with this last point, for instance, it is frequently possible to share a telephone channel with one or more functions, such as supervisory control or transfer trip, when the brief and infrequent interruptions of the voice channel can be tolerated.

It is easy to gain the impression from a quick reading of the paper that any and all applications such as listed in the opening of the paper can be handled by the same equipment. While this is entirely feasible, it may not be necessarily the most economic arrangement nor the most suitable from the standpoint of channel-space conservation. Simple applications requiring only a few joint-use channels may be handled with simple transmitter and receiver equipment such as generally used for pilot-relaying channels. More complex applications requiring more than two or three joint-use or simultaneous functions may require more complex basic transmitter and receiver equipment. Experience of several years has shown that the rack-and-panel unit type of construction is ideally adapted to provide maximum flexibility and economy in meeting this wide range of functional requirements.

Starting with the standard wide-band or voice-channel type of transmitter and receiver equipment, it is a relatively simple matter to provide as many as eight or ten simultaneous and independent impulse signal circuits over a single carrier as indicated by the authors. It is also possible to use the wide-band equipment to transmit a voice channel and one or more tone-frequency channels simultaneously. However, it should be realized that in sharing a channel that normally is just wide enough for a good commercial-quality telephone circuit with one or more audio-tone signals, some appreciable reduction in the grade of voice communication must be accepted. If the tones are placed below the voice band, some loss in naturalness will result and some interference from the tone channels is to be expected, since it is extremely difficult to prevent the generation of harmonics of the tone signals in the receiver. If the tones are placed above the voice band, some loss in quality also will result, or special means must be taken to widen the channel.

Although the authors do not attach any particular significance to the simple frequency-modulation scheme described, in view of the extensive interest in the use of frequency modulation for both radio and power-line carrier, it should be pointed out that this simple arrangement does not utilize the two basic elements from which frequency modulation derives its principal advantages, namely, the limiter and the balanced discriminator. Also, it would appear that the simple demodulation scheme might be troublesome in the case of three or four terminal pilot-relaying installations, where it is often necessary to space transmitters by 500 cycles or so to avoid zero beat between two transmitters.

I believe it would be of general interest if the authors would indicate whether or not the novel scheme illustrated by Figure 9 of the paper has been tried out on an actual installation. Offhand it would appear generally impractical to receive in one receiver three separate signals originating at different stations and travelling over three different and variable transmission paths. Even though the three transmitters may be adjusted to put substantially the same signal level into the receiver under one set of line conditions, changes in the line resulting from switching will normally change the relative levels over a considerable range. The automatic volume control will be controlled by the strongest received carrier, and if there is more than a few decibels difference between the three signals, one or both of the weaker ones may be lost. Even when the three signals are being received at about the same level, the 1.5 kilocycle and 3-kilocycle heterodyne between them will have an effect similar to 100 per cent modulation of each carrier at the beat frequencies. This effect will cause the three telemeter signals to interreact in such way as to make it difficult to maintain uniform relay currents in the three telemeter receivers.

REFERENCE

1. MULTICHANNEL CARRIER-CURRENT FACILITIES FOR A POWER LINE, **P. N. Sandstrom, G. E. Foster.** AIEE TRANSACTIONS, volume 61, 1942, February section, pages 71–6.

Townsend D. MacCoun (Link Radio Corporation, New York, N. Y.): The authors are to be congratulated on this very excellent paper. It is readily apparent that such a system of using tones is very economical, and where multiple functions are to be performed the costs would be easy to justify.

In considering such a system however, there are certain inherent limitations which also should be considered. The first of these is the question of the stability. The authors point out that a frequency drift of plus or minus six per cent could be tolerated, and that this would mean holding a carrier system at 100 kilocycles to plus or minus 45 cycles. Besides the drift in oscillator circuits, the drift in the tuned circuits of the tone filters also must be considered, and as this drift could be on approximately the same order as the oscillator drift, this will reduce the operating margin materially. On the converse, in a carrier-operated system using quartz-crystal control in the transmitter and a crystal filter in the receiver, experience has shown that it is not at all difficult to maintain this stability within plus or minus two cycles over wide temperature changes. It is a factor whose importance increases when the equipment is to be used outdoors, as is often the case.

The ability of the system to discriminate against noise is governed not only by the steady-state characteristics of the selectivity of this system but also is determined by the transient characteristics. Unfortunately, as the selectivity is increased, the shock-excitation characteristics create a difficult problem. Transient impulses such as produced by the operation of breakers, lightning arresters, and the like, will set a whole tuned circuit into oscillation and will result in false output pulses being received. This effect, of course, is very pronounced in a crystal-filter circuit where the selectivity is extremely high, but several methods have been found to overcome this. By using two carrier frequencies close together it is readily possible to operate under conditions where the noise is on the order of 100 times the received signal.

Another limitation of course is the low percentage of modulation necessary to prevent the overmodulation of the carrier when the various tones are simultaneously transmitted, and whereas it is noted that the receiver the authors used will operate through a loss of 70 decibels, it is probable that with a higher percentage of modulation this might be increased to 90 or 100 decibels.

H. W. Lensner and **J. B. Singel** (nonmember): The chief application for audio tones on power-line carrier is found in cases where it is necessary to perform two or more functions simultaneously. There are cases where communication, telemetering, and supervisory control can be added to a carrier relaying installation using a single unmodulated carrier frequency, provided that it is allowable to interrupt telemetering for any one of the other functions. When several functions must be in use simultaneously, or where additional reliability is desired, the use of audio tones is justified. This is particularly true on large power systems where the available carrier channels are being exhausted and it is necessary to get the maximum utility from each carrier channel.

For many applications, standard relaying carrier equipment can be used for the added functions. In complex applications involving extensive use of tones, more advanced equipment is warranted. This fact was recognized in the recommendation of a superheterodyne receiver for the extensive use of tones. Also, this is the only condition under which a low percentage modulation per tone is necessary. The fewer the tones, the higher the allowable modulation per tone. With two or three tones, each tone can modulate the carrier 30 to 50 per cent. With ten tones, a more powerful transmitter is desirable to give the same sideband energy per tone as with two or three tones on a standard relaying transmitter. This will allow the receiver to operate through approximately the same decibel loss, regardless of the number of tones.

It is better to have intelligibility rather than high quality in a carrier communication circuit over which important instructions may be given, and for this reason the audio tones have been located at the low-frequency end of the audio channel. Eliminating low frequencies does result in some loss of naturalness, but eliminating frequencies at the high end causes a high loss of intelligibility which is much worse. For example, cutting out all frequencies below 500 cycles per second reduces the intelligibility only 4 per cent, whereas eliminating frequencies above 2,500 cycles causes a loss of 20 per cent in intelligibility.

One of the main reasons for using frequency modulation is the limiting action of the simple relay receiver, particularly when a strong signal is being received. The detector tube, in conjunction with a voltage-limiting neon lamp across the tuned input circuit, performs very effectively as a limiter, and cuts off amplitude variation on the received signal. Under this condition, only a frequency modulated signal can be received satisfactorily, and the sloping side of the receiver resonance curve allows demodulation. This system was described as providing for emergency communication (as, for example, when adjusting and tuning the equipment), and is not intended to give results comparable to standard carrier communication equipment.

With regard to the discussion of frequency drift of the carrier- and tone-frequency components, the figures given in the paper were for purpose of illustration only, and should not be considered indicative of performance of modern equipment.

The authors question the meaning of the statement that by using two carrier frequencies close together it is possible to operate under conditions where the noise is on the order of 100 times the received signal. It is not obvious why crystals operating at carrier frequencies close together should relieve the shock-excitation difficulty resulting from high-amplitude noise. There appears to be only one possible interpretation under which such a statement could be correct. If the noise referred to is the noise on the line in the entire carrier frequency band from 50 to 150 kilocycles, then a single tone filter that has a pass band about 100 cycles wide, will accept only 0.001 of the total noise but all of the sideband energy of its tone. This will give a signal-to-noise ratio at the grid of the filter detector tube of better than unity, depending on the band width of the tone filter.

The scheme of Figure 9 of the paper will require the selection of a frequency on which the attenuation over each path does not vary more than a few decibels for different conditions of system switching. Such a frequency could be determined by making a frequency run, measuring the line loss for each system condition, and then selecting a frequency where the attentuation remains constant within a few decibels.

System Voltages and Fault Currents Determined Easily on D-C Board

Discussion and author's closure of paper 44-68 by Carl E. Asbury, presented at the AIEE winter technical meeting, New York, N. Y., January 24-28, 1944, and published in AIEE TRANSACTIONS, 1944, April section, pages 184-8.

G. W. Hampe (Commonwealth Edison Company, Chicago, Ill.): Mr. Asbury has pointed out some uses of the d-c calculating board which generally are not recognized. Apparently, Mr. Asbury left the matter of reading the residual currents directly in amperes, that is, with one board milliampere representing, say, ten line amperes, as distinct from reading unit currents on a given kilovolt-ampere base, up to the reader's discretion. The use of unit currents is common for three-phase faults.

By a proper choice of applied voltage, one can take the readings either way without changing the base of the board setup. If the ground-fault study is for a system which is all at one voltage level, it is usually more convenient to read amperes directly. However, if different voltage levels are involved, or if the three-phase faults were studied in kilovolt-amperes and phase-relay settings were made in kilovolt-amperes, or if ground relays are of minor importance in the study, then it may be more practical to have the ground currents in unit (kilovolt-ampere) values.

Mr. Asbury refers to special scales for the galvanometer. Perhaps special scales for the voltmeter would be more practical. When a d-c board is used for a single utility system or interconnected group, as distinct from manufacturing or consulting practice, it becomes practical to have special percentage scales put on the board voltmeter with the unity or 100 per cent point at the same deflection as the unity voltage for the current readings. Thus, resetting the applied voltage when changing from current readings to voltage readings is avoided.

An extension of Mr. Asbury's method is needed for a system which is so large that the positive-sequence network occupies the entire board. It then becomes necessary to reduce the positive-sequence network, which in Mr. Asbury's procedure represents the negative sequence also, to a single equivalent impedance, or if loads are represented in the positive network, to reduce it to a Thevenin equivalent. The equivalent is then placed in series with the zero-sequence network for the ground-fault setup.

The procedure 6 in Mr. Asbury's paper under "Description of Method" still can be carried out to yield the various values of V_0 in the zero-sequence network. The value of the drop across the entire zero-sequence network, which may be called V_0', also can be measured.

It is possible also to measure in per cent the entire drop V' across the impedance which represents the positive- and negative-sequence networks. The values of V in per cent in the positive-sequence network, as read in Mr. Asbury's procedure 4, should be available from a previous three-phase fault study. The value V' represents the per cent by which the voltage applied to the positive-sequence network is reduced in procedure 6 as compared with the three-phase fault study (procedure 4). The value of V_{12} (procedure 7) then can be computed by the formula

$$V_{12} = VV' + V_0'$$

The writer considers it desirable that the manufacturers of d-c calculating boards make their models somewhat more adaptable for such work as Mr. Asbury describes. The board which the writer uses has been adapted specially in the following ways:

1. Small slide wires are mounted in the rear of the board sections. One terminal is tied solidly to the board negative bus, and the other terminates in a standard cord and plug at the front. In the positive-sequence network the reactive component of the load adjacent to a given bus is applied by means of these auxiliary load circuits.

2. Provision is made for reading the voltage distribution on board busses by means of the built-in table meters. A special selector switch has been added which can be used to connect a voltmeter terminal to one galvanometer terminal, and the other voltmeter terminal to board negative, board positive, or other special board busses as desired. By setting this switch, voltages are read in the same manner as currents, that is, when a metering switch for a given circuit is operated, the voltage at the terminal of that circuit is indicated.

The foregoing modifications have been in use for several years and have been found so advantageous that they should be made generally available by manufacturers of d-c boards.

Edward W. Kimbark (Massachusetts Institute of Technology, Boston, Mass.): The method described in the paper for determining voltages and currents in a three-phase network subjected to a line-to-ground fault is not new. I used it on a d-c board in 1937, and it was not new then.

Although the method may be explained in terms of symmetrical components, as it is explained in the paper, a much better explanation is that given by Miss Clarke under the designation "use of sum and difference of positive- and negative-sequence symmetrical components."[1] According to her explanation, three substitute networks are used, the impedances, voltages, and currents of which are given in Table I of this discussion. The last two networks contain equally generated electromotive forces. A line-to-ground fault is represented by connecting the zero and sum networks in series at the point of fault and leaving the difference network open there.

Table I

Network	Impedance	Voltage	Current
Zero	$Z_0/2$	V_0	$2I_0$
Sum	$Z_1 = Z_2$	$V_1 + V_2$	$I_1 + I_2$
Difference	$Z_1 = Z_2$	$V_1 - V_2$	$I_1 - I_2$

On the d-c board, loads and other shunt branches usually are omitted, and all generated electromotive forces necessarily are assumed in phase with each other. Therefore, the difference network, not being involved in the fault, has zero current in all its branches, and it has a voltage equal everywhere to the generated electromotive force. Consequently, the difference network need not be set up and measured.

On the a-c board, on the other hand, it is possible and often desirable to represent loads and phase differences. The normal load voltages and currents then appear in the difference network and may be read there. The same network, set up on the board, may be used in succession as the difference network (open at the fault) and as the sum network (connected to the zero network at the fault).

Phase voltages and currents can be calculated from the zero, sum, and difference voltages and currents. The sum and difference voltages both appear plainly in the equations below equation 5 of the paper. Further below, the value 1.00 is substituted for the difference voltage, which is appropriate for a d-c board.

Reference

1. Determination of Voltages and Currents During Unbalanced Faults—Use of the Sum and Difference of Positive- and Negative-Sequence Symmetrical Components, Edith L. Clarke. *General Electric Review*, volume 40, 1937, pages 511-13.

Max A. Faucett (University of Illinois, Urbana, Ill.): The author is to be commended for this paper concerning studies on a calculating board. No doubt the author does not claim that this paper is intended to reveal any revolutionary method in calculating results of board studies, but rather to simplify the work involved in calculating relay settings. The careful selection of a kilovolt-ampere base for converting all impedances and the use of a voltage on the board which will simplify the calculations, obviously, are important. It is suggested that the author should stress very strongly that vector relationships exist and that the data which has been expressed on a per cent base should not be misinterpreted. To emphasize this point, it is believed that the author should expand item 9 in the section discussing "Description of Method."

To assist the inexperienced relay engineer, two other suggestions are offered:

1. To draw simple electric-circuit diagrams for each figure to conform with the board-circuit diagram and mark all voltage and current components.
2. To use some actual board test data from the author's experience and give a complete analysis of these data for the various fault conditions.

As stated previously, the author has written an excellent paper, but it is believed that many readers will be helped by an expansion of the items mentioned.

Carl E. Asbury: With reference to G. W. Hampe's discussion, the author's intention was to leave the matter of converting the board readings into units most suitable for the reader's purpose. This will depend on the design of the particular board being used and the marking of the galvonometer scale. A scale may be marked to read directly in kilovolt-amperes, amperes, or it may be necessary to multiply the board reading by a constant to convert it to either of these units. The scale also may be calibrated in per unit values. The advantage of one over the other should be considered when a board study is being planned, and the selection of units, as Mr. Hampe pointed out, should depend on the particular system involved and the form in which the final data are desired.

If a second galvanometer scale is provided to read 50 per cent higher than the first and marked in the same units, it may be used to take readings for ground faults. These readings, when divided by the same constant used to convert the three-phase-fault kilovolt-amperes (kilovolts $\sqrt{3}$ if scales are marked in kilovolt-amperes) into amperes per conductor, will give the residual amperes flowing for a single line-to-ground fault.

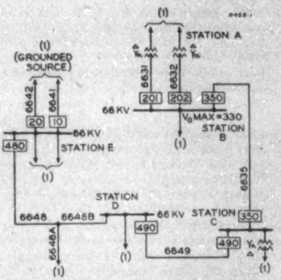


Figure 1. Network used for simple calculations

(1). To other networks and generators. All grounded sources shown on the diagram

If the galvanometer scales are marked in kilovolt-amperes, this method permits the recording of readings at any point in the system for three-phase and phase-to-ground faults without using different constants to correct the board readings for the various voltage levels. The same constant then can be used to multiply readings for both three-phase and phase-to-ground faults to convert them to amperes at the proper voltage.

In referring to the use of the 3/2 scale for reading ground-fault data as mentioned in procedure 5 under "Description of Method" and also mentioned under the subheading "Phase-to-Ground Fault," it appears that the reason for the use of this scale may have been misinterpreted. The principal purpose of using the 3/2 scale is to give board readings for phase-to-ground faults in equivalent units to those obtained when reading three-phase faults. For example, if the galvanometer scale is marked to read kilovolt-amperes directly for three-phase faults, the reading obtained by using this method on a 3/2 scale during a phase-to-ground fault might be termed as equivalent three-phase kilovolt-amperes for a ground fault, which means only that the reading obtained on the 3/2 scale for the phase-to-ground fault divided by kilovolts $\sqrt{3}$ would give residual amperes. This same constant of kilovolts $\sqrt{3}$ divided into the reading obtained for the three-phase fault gives amperes per conductor during the three-phase fault. As previously mentioned, this has the advantage of reducing the number of constants required to convert the board readings into amperes, for both three-phase and phase-to-ground faults, which is a very desirable feature. The extension which was provided by Mr. Hampe for items 6 and 7 under the "Description of Method" in the paper would be very helpful where the system involved is too large to be set up in its entirety with one board set up. This refers to his method of obtaining V_{12} from the equation $V_{12}=VV'+V_0'$.

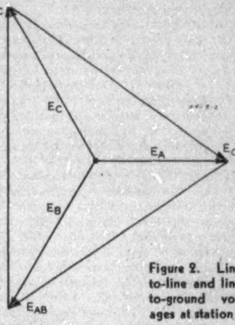


Figure 2. Line-to-line and line-to-ground voltages at station *E*

Another procedure, which often may prove helpful when crowded for room on the board, is to set up the positive-sequence network in its entirety and take three-phase faults on the portions where ground faults may not be of interest. This portion of the positive-sequence network may be replaced by a more simple equivalent network or perhaps one equivalent generator, and the board resistors which were used formerly to set up this portion of the positive-sequence network in detail can be released for use in the zero-sequence network. Often, where ground-fault studies are to be made on different voltage levels which have their zero-sequence networks isolated from each other, the complete positive-sequence network for all voltage levels may be set up, the zero-sequence network set up for one voltage level, and the complete study may be made on that part of the system. This zero-sequence network then may be dismantled and used to set up the zero-sequence network for another voltage level of the system.

The author is in agreement with Mr. Hampe's comments that it would be very desirable that manufacturers of d-c calculating boards design their models to be more adaptable to methods of making short-circuit studies similar to the one outlined in the paper. This should include the installation of a high-resistance voltmeter as part of the standard board equipment in addition to the regular galvanometer, and it would be very desirable to have some type of switching arrangement worked out for the voltmeter, so the various desirable voltages can be obtained quickly.

In E. W. Kimbark's discussion, it is pointed out that the method is not new, and that he made use of it in 1937 in a d-c board study. Since d-c boards have been in use for many years for short-circuit studies, and

Table II

		Three-Phase Short Circuit				Ground Fault			
Station and Oil Circuit Breaker	Kilovolts	Rheostat Number	Minimum Kilovolt-Amperes	Maximum Kilovolt-Amperes	Maximum Amperes	Rheostat Number	Reading	Residual Amperes	Phase Amperes
C −350	66	40	6.5	24.3		114	25.0		
C Bank		7	77.3	78.5		111	129.0		
C −490		9	56.2	71.4		110	23.5		
Total			35×4	43.5×4			177.5		
E −480		13	63.0	59.0		97	23.3		
E Voltage	66			$V=84.2$				$V_{12}=89.5$	$V_0=5.0$
B −201		41	32.5	32.5		123	12.0		
B −202		43	43.3	42.3		121	12.2		
B Voltage	66			$V=80.5$				$V_{12}=86.8$	$V_0=3.5$

Table III

Station and Oil Circuit Breaker	Kilovolts	Three-Phase Short Circuit: Rheostat Number	Three-Phase Short Circuit: Minimum Kilovolt-Amperes	Three-Phase Short Circuit: Maximum Kilovolt-Amperes	Three-Phase Short Circuit: Maximum Amperes	Ground Fault: Rheostat Number	Ground Fault: Reading	Ground Fault: Residual Amperes	Ground Fault: Phase Amperes
C −350	66	40	6,500	24,300		114	25.0	219	217
C Bank		7	77,300	78,500		111	129.0	1,131	845
C −490		9	56,200	71,400		110	23.5	206	494
Total			140,000	174,200	1,530		177.5	1,556	
E −480		13	63,000	59,000		97	23.3	204	420
E Voltage	66			$V=84.2$				$V_{12}=89.5$	$V_0=5.0$
B −201		41	32,500	32,500		123	12.0	105	228
B −202		43	43,300	42,300		121	12.2	107	287
B Voltage	66			$V=80.5$				$V_{12}=86.8$	$V_0=3.5$

since the method of symmetrical components likewise have been in use for a number of years, the author thought that it was so obvious that no radically new ideas were involved that it was not necessary to make mention of the fact. However, if Mr. Kimbark was present during the presentation of the paper at the AIEE winter technical meeting he should recall that the author opened the paper with a statement to this effect. This apparently was quite obvious to other discussers of the paper, since they repeat the author's purpose in preparing the paper. The author suggests that Mr. Kimbark again read the opening paragraph of the paper which explains its purpose. The opening sentence of Mr. Hampe's discussion states that this particular method points out a number of uses for the d-c calculating board which generally are not recognized. Since d-c calculating boards are in quite common use for relay co-ordination studies, there seemed to be a definite need for a description and discussion of a method which could be used in making d-c board studies with the practical application of the information kept in mind for general use by relay engineers. Many discussions of symmetrical components approach the problem purely from a mathematical viewpoint and fail to point out the direct application.

With reference to the discussion by Max A. Faucett, it probably would be helpful to those unfamiliar with the method to show a simple network with a few board readings and the calculations necessary to convert these readings into the desired data. A portion of a network is shown in Figure 1 of this discussion. Table II of the discussion shows board readings for a three-phase and a phase-to-ground fault on the 66-kv bus at station *C*. This is followed by sample calculations showing the steps necessary to convert board readings into the desired data. Table III shows the tabulation of the completed data. This provides the desirable expansion of the method, as suggested in Mr. Faucett's discussion. For the particular system shown in Figure 1, only reactance values were used, since they differed very little from the circuit impedance. However, the author wishes to emphasize the importance of choosing either reactance or impedance where voltage measurements are to be taken. This is discussed in the paper under the subheading "Using Reactance or Impedance."

CALCULATIONS

The galvanometer scale used for the three-phase-fault data was marked in thousands of kilovolt-amperes. Any shunt constants for the galvanometer are recorded with the reading. The scale used for ground-fault data equals 3/2, that used for the three-phase faults. The total board voltage was 100 volts.

Three-Phase Short-Circuit Data.

Kva = board reading × 1,000

$$\text{Maximum amperes}=\frac{\text{kva}}{\text{kv}\sqrt{3}}=K_1\times\text{kva}$$

(K_1 = constant and varies with voltage)
($K_1=8.77\times10^{-3}$ for 66 kv)

Ground Fault. Residual amperes

$$=\frac{\text{board reading}\times1{,}000}{\text{Kv}\sqrt{3}}$$
$$=K_1\ \text{board reading}\times1{,}000$$

$$\text{Phase amperes}=I_{\phi_1}=\frac{I_{GT}}{3}\left[2\frac{R_{P1}}{R_P}+\frac{R_{G1}}{R_G}\right]$$

$$I_{\phi_1}=\frac{1{,}556}{3}\left[2\times\frac{24.3}{174.2}+\frac{25}{177.5}\right]$$
$$=519\ (0.278+0.141)=519\times0.419=217$$

Calculations of I_ϕ may be of interest only at a few locations in the system.

$$I_1=I_2=\frac{I_G}{3}\times\frac{R_{P1}}{R_P}\qquad I_0=\frac{I_G}{3}\ \frac{R_{G1}}{R_G}$$

For station *B*—oil circuit breaker (350).

$$I_1=I_2=\frac{1{,}556}{3}\times\frac{6.5}{35\times4}=240\ \text{amperes}$$

$$I_0=\frac{1{,}556}{3}\times\frac{25}{177.5}=73\ \text{amperes}$$

CALCULATIONS OF VOLTAGES

Three-Phase Fault. Voltage at station *B* = 80.5 per cent of normal (from readings)

If we express vectorially (in per cent)

(*A, B, C* sequence)
$V_{AB}=80.5(1.+J0)$
$V_{BC}=80.5(-0.5-J0.866)$
$V_{CA}=80.5(-0.5+J0.866)$

Voltage at station *E* = 84.2 per cent.
If we express vectorially (in per cent)

(*A, B, C* sequence
$V_{AB}=84.2(1+J0)$
$V_{BC}=84.2(-0.5-J0.866)$
$V_{CA}=84.2(-0.5+J0.866)$

Line-to-Ground Fault. Consider fault on line *A* to ground.

Station *B* (voltages line to ground)

$E_A=V_{12}-V_0=86.8-3.5=83.3$ per cent

$E_A=83.3(1+J0)$ per cent

$$E_A=0.833\times\frac{66{,}000}{\sqrt{3}}=31{,}700+J0\ \text{volts}$$

$E_B=-3.5-0.5\times86.8-J86.6$
$=-46.9-J86.6$ per cent

$$E_B=(-46.9-J86.6)\frac{66{,}000}{\sqrt{3}}$$
$=-17{,}800-J33{,}000$ volts

$E_C=-3.5-0.5\times86.8+J86.6$
$=-46.9+J86.6$ per cent

$$E_C=(-46.9+J86.6)\frac{66{,}000}{\sqrt{3}}$$
$=-17{,}800+J33{,}000$ volts

Sequence Voltages at Station B.

$$V_2=\frac{E_G-V_{12}}{2}=\frac{100-86.8}{2}$$
$$=\frac{13.2}{2}=6.6\ \text{per cent}$$

$V_1=V_{12}+V_2=86.8+6.6=93.4$ per cent
$V_0=3.5$ from board readings

Volts on ground relay at station $B=3.5\times330=11.55$ volts. Note that $E_1+E_2+E_0=E_A=93.4-6.6-3.5=83.3$ per cent. Note that V_1, V_2, and V_0 give magnitude only. The vector values of each may be written by using $a=-0.5+J0.866$ for V_1 and V_2 of the unfaulted phases.

Voltages Line to Line at Station B.

$E_{AB}=E_B-E_A=(-46.9-J86.6)-$
$(83.3+J0)=-130.2-J86.6$ per cent
$E_{BC}=E_C-E_B=(-46.9+J86.6)-$
$(-46.9-J86.6)=0+J173.2$ per cent
$E_{CA}=E_A-E_C=(83.3+J0)-$
$(-46.9+J86.6)=130.2-J86.6$ per cent

Station E (Voltages Line to Ground).

$E_A=V_{12}-V_0=89.5-5=84.5$ per cent
$E_A=84.5(1+J0.)=84.5+J0$
$E_B=-V_0-0.5V_{12}-J86.6=-5.0-0.5\times$
$89.5-J86.6=-49.75-J86.6$ per cent
$E_C=-V_0-0.5V_{12}+J86.6=-49.75+J86.6$ per cent

Sequence Voltages at Station E.

$$V_2=\frac{E_G-V_{12}}{2}=\frac{100-89.5}{2}=5.3\ \text{per cent}$$

$V_1=V_{12}+V_2=89.5+5.3=94.8$ per cent
$V_0=5$ per cent from board reading

Negative-sequence voltage on filter to negative-sequence ground relay at station $E=$

$$5.3\times\frac{66{,}000}{600}=5.83\ \text{volts.}$$

Voltages Line to Line at Station E.

$E_{AB}=E_B-E_A=(-49.75-J86.6)-$
$(84.5+J0)=-134.2-J86.6$ per cent
$E_{BC}=E_C-E_B=(-49.75+J86.6)-$
$(-49.75-J86.6)=0+J173.2$ per cent
$E_{CA}=E_A-E_C=(84.5+J0)-(-49.75+$
$J86.6)=134.2-J86.6$ per cent

(See Figure 2 of this discussion.)

Effect of Altitude on Impulse and 60-Cycle Strength of Electrical Apparatus

Discussion and authors' closure of paper 44-67 by P. L. Bellaschi and Paul Evans, Jr., presented at the AIEE winter technical meeting, New York, N. Y., January 24–28, 1944, and published in AIEE TRANSACTIONS, 1944, May section, pages 236–41.

V. M. Montsinger (General Electric Company, Pittsfield, Mass.): I would like to ask the authors if they expect to take steps to have the results of their work incorporated in the proper AIEE Standards.

I note that in several cases the impulse spark-over and flashover does not come down as rapidly as the air density. This is particularly true of the front of wave spark-over tests shown in Figure 12 of the paper, where the relative voltage is approximately 0.8 when the relative air density is 0.6. Also, the critical impulse flashover in several cases does not decrease as fast as the air density.

It appears that where the flashover does not decrease in proportion to the air density it would be well to recognize this fact in the Standards.

F. J. Vogel (Illinois Institute of Technology, Chicago, Ill.): The data and references in this paper are very valuable since they show the effect of the reduction in pressure on the dielectric strength of various parts of transformers and other apparatus.

Since many transformers may be used at high altitudes and be protected only by gaps, it is of interest to see what the various comparative values are.

For example, for $1^1/_2$x40-microsecond impulse waves, the oil-insulated parts lose very little strength with reduced pressure, whereas the air-insulated parts lose very much. Therefore, oil-insulated apparatus would be protected against $1^1/_2$x40-microsecond impulses even with increased air clearances at high altitudes. On the other hand, for steep wave fronts, the strength of air gaps is affected less than for $1^1/_2$x40-microsecond waves, and it would not be desirable perhaps to lengthen such gaps. However, if the gaps were not lengthened, there probably would be more failures from switching surges and other disturbances.

The conclusion can be drawn that air-gap protection is not likely to be so successful at high altitudes as at sea level. This statement does not imply that air-gap protection alone is satisfactory, but is intended to be comparative only. I would like to ask if the authors agree with this conclusion.

Also, I note that the authors have covered impulse and 60-cycle strengths in general, but not switching-surge strengths or the effect of precipitation. Some tests on the effect of low pressure and precipitation on insulators were reported by Peek many years ago, but there are no recent data and I think it would be desirable to complete the story.

R. C. Mason (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The long time lags for breakdown observed by the authors are quite familiar to any one who has worked with discharges at low pressures. As they state, the reasons are understood, at least in a rough, qualitative way. Two causes apparently contribute: a formative time lag, which appears to involve waiting for suitable initiating ions to be produced by external agents; and, at lower pressures, an appreciable time for the accumulation of space charge and dropping of voltage across the gap. Both effects seem to have been observed by the authors.

Because of the long time lags, at low gas density, the data obtained with a 1.5x40-microsecond wave does not always reveal the full effect of reduction of gas density; when breakdown takes place so far on the tail of the wave, the peak voltage is considerably greater than the voltage at which the gap begins to conduct, and also greater than the d-c or 60-cycle breakdown. With direct current applied, the breakdown voltage for uniform field decreases just slightly less rapidly than gas density; for nonuniform fields, the rate is still less. When breakdown occurs on the front of the wave, the long time lag causes much higher voltages to be registered than for direct current; hence the relatively flat curves of Figure 12 in the paper and the smaller *RAD* correction mentioned in the paper. The impulse ratio for steep wave fronts must be much larger at very low pressures than at atmospheric pressure.

It seems barely possible that laboratory tests made under reduced pressure, but at low altitudes, may not duplicate adequately conditions at high altitudes for impulse tests. One would not expect the probable considerable difference in extraneous ionizing agents at extreme altitudes, as mentioned by the authors, to show any very large effect on d-c breakdown voltage. For impulses, however, where so much depends on time lag, and consequently on residual ion density and rate of formation of ions, one cannot be sure without actual experiments that results will be the same at all altitudes.

Victor Siegfried (Worcester Polytechnic Institute, Worcester, Mass.): In support of the authors' conclusions, I would like to call attention to the tests made in selection of conductors for the Boulder Dam–Los Angeles transmission line,[1] and to the discussion appended to that paper.[2] The need for consideration of the relative air density was recognized, and its effect upon the starting voltage of corona was given much attention. From tests by this discusser and A. K. Nuttall, respectively, in which the *RAD* was lowered by an evacuated tank and by raising conductor temperature, it was found that corona starting voltage was decreased materially. The amount of this decrease was found to be not in exact proportion to *RAD*, but rather to be such that corona starting voltage is proportional to $RAD^{2/3}$.

This exponent of $^2/_3$ is borne out somewhat by the data of this paper, in which values of breakdown voltage do not decrease as rapidly as the *RAD*, as for example in Figure 14 of the paper. The work on corona deals principally with the strength of air, whereas this present paper deals with breakdown and flashover strengths of apparatus, with other factors such as gap configurations entering into the picture. The authors' interpretation of these data as indicating a linear proportion essentially, is probably the better one for this consideration, since it will lead to a more conservative rating of the apparatus.

It is gratifying to see these data carried to the equivalent altitudes of 50,000 feet, as the transmission-line work was carried to an equivalent of only 15,000 feet.

References

1. Corona Loss Measurements for the Design of Transmission Lines to Operate at Voltages Between 220 Kv and 330 Kv, J. S. Carroll, Bradley Cozzens. AIEE Transactions, volume 52, 1933, pages 55–62.

2. Discussion by **W. S. Peterson** of reference 1. AIEE Transactions, volume 52, 1933, pages 62–3.

M. B. Mallett (English Electric Company of Canada, Limited, St. Catherines, Canada): With electrical apparatus immersed in gas at pressures above 760 millimeters, present knowledge of the subject indicates that, as the gas pressure is raised, the impulse strength of creepage surfaces increases in general at a slower rate than that of gaps. Thus the designer of gas-immersed apparatus, as for example, high-voltage transformers, would co-ordinate strike and creepage allowances in the physical structure for the particular gas pressure involved.

The above consideration suggests the possibility of a similar effect in apparatus immersed in air at pressures below 760 millimeters. That is, as the air pressure is lowered, the impulse strength of creepage surfaces would decrease more slowly than that of gaps. If such a condition obtained, it might be found desirable to change the relation between creepage and strike allowances, as compared to that of standard air-immersed apparatus.

It appears that flashover tests described in the paper were confined to gaps, apparatus surfaces involving strike and creepage in parallel, and wood surfaces. Since the behavior of wood largely must be discounted, it would seem that these tests do not segregate directly the effect of low air pressure on creepage strength. The group of curves in Figure 10 of the paper, however, suggests to the writer that creepage strength probably is maintained to a greater extent than gap strength, as the pressure is lowered.

In the event of further investigation, it is suggested that additional information on this creepage situation would be desirable.

P. L. Bellaschi and **Paul Evans, Jr.**: In future standardization work, cognizance should be taken of the data and conclusions in the paper. This is the point Mr. Montsinger has raised. In so doing, we suggest that the over-all problem of apparatus application at high altitude be kept in mind.

In repy to Mr. Vogel, rod-gap spacings suitable for front-of-wave impulse protection of transformers at sea level would provide equal protection at high altitude (Figure 11A and Figure 12 of the paper). In fact the front-of-wave gaps may be increased a small amount, but this increase alone would not compensate for the lower spark-over on switching surges. We expect the rod-gap curve for switching voltages to follow the curves in Figure 10. It is possible that at high altitude the gaps may require opening to take care of switching voltages and yet provide protection against lightning. Lightning apparently is not so severe at high altitude as at sea level (reference 24 of paper). In any event the modern arrester properly applied is suitable to protect apparatus even at high altitude.

As to the effect of rain at high altitude, 60-cycle voltages alone need be considered. Tests of this character probably could be made conveniently on actual apparatus at high altitude. Lacking such data, it is a reasonable assumption that wet flashover likewise is reduced in direct proportion to *RAD*.

Doctor Mason's discussion of the physical factors associated with breakdown of air at the low pressures is interesting. In principle it follows the viewpoint that guided the authors in their statements.

Professor Siegfried has reference to the corona voltage of line conductors as affected by the air density. It is rather difficult from the 60-cycle flashover data of the apparatus in Figure 14 of the paper to correlate the two other than by indirect deductions. It would seem that the corona-voltage relation of conductor, in terms of the two-thirds power of the *RAD* and as given by Peek's formula (reference 4 of paper), give practically the same results. The former relation, however, is simpler.

In reference to the question Mr. Mallett has raised, curve *B* of Figure 10 and curve *G* of Figure 11 of the paper represent data for a straight creepage surface. The other curves represent straight jump and combinations of jump and creepage. Practically speaking, all are affected by the *RAD* to the same relative amount. The only part which is affected inappreciably is the liquid and solid insulation.

Overloading of Transformers—Cases Not Covered by the General Rules

Discussion and authors' closure of paper 44-66 by W. C. Sealey and J. B. Hodtum, presented at the AIEE winter technical meeting, New York, N. Y., January 24–28, 1944, and published in AIEE TRANSACTIONS, 1944, April section, pages 149–53.

H. K. Sels (Public Service Electric and Gas Company, Newark, N. J.): In view of present world-wide conditions, the question of overloading transformers and similar equipment has received considerable attention in the past few years. Many methods of handling the problem have been presented, of which the paper by W. C. Sealey and J. B. Hodtum covers some of the exceptional cases.

It has been our experience during an emergency that it is better to spend less time determining the allowable loads which can be carried on the remaining equipment and more time removing and replacing the equipment in trouble. Most of the methods presented require "on the spot" readings, calculations, and assumptions, and in order to eliminate these requirements, a method of establishing an emergency rating for a transformer before the emergency occurs is suggested.

The transformer subcommittee interim report of June 22, 1942, proposed an emergency loading of transformers with moderate sacrifice of life expectancy. These emergency loads are based on a maximum hottest-spot temperature for a given time duration. The highest temperature recommended is 137 degrees centigrade for one hour, with other maximum hottest-spot temperatures of lower values for longer durations—the lowest value being 110 degrees centigrade for 24 hours. Any one application would decrease the expected life of the transformer approximately one per cent. These values are considered conservative, and are based on the life-expectancy curve published by T. C. Nichols, which gives 123 degrees centigrade hottest-spot temperature for twenty-four hours as one per cent loss of life.

By using one per cent loss of life for any one emergency, and the assumption that the duration will not exceed one day, a starting point can be established. Then the hottest-spot temperature variation of the transformer can be calculated by using the most severe daily-load factor and an assumed overload. This hottest-spot temperature range can be equated to equivalent life. By determining the overloads that will give one per cent loss of life for various ambient temperatures, a curve or chart can be made up to be used by the station operating personnel.

The results of such a calculation for one of our stations having a 72 per cent daily load factor and fan-cooled transformers are shown in Table I.

Table I

Ambient Temperature (Degrees Centigrade)	Emergency Fan-Cooled Peak-Overload Rating in Per Cent of Self-Cooled Full-Load Rating
10	200
20	194
30	186

The hottest-spot temperature for the above case ranged between 140 degrees centigrade, maximum and 63 degrees centigrade, minimum, or an equivalent aging hottest-spot temperature for the day of 123 degrees centigrade.

In order to show the importance of considering the daily load variation, Table II covers the same transformer for a station with an assumed 90 per cent daily load factor.

Table II

Ambient Temperature (Degrees Centigrade)	Emergency Fan-Cooled Peak-Overload Rating in Per Cent of Self-Cooled Full-Load Rating
10	191
20	184
30	176

It may be argued that some emergencies last less than one day, but an examination of the permissible overload ratings will indicate that any material increase will endanger other equipment if it has not done so already.

While the transformer subcommittee may desire to adhere to their original recommendation of hottest-spot temperatures, we suggest that they also recommend a method of establishing an overload rating which can be used by operating personnel to meet specified emergencies, which will take into consideration load factor and one per cent aging for each twenty-four hours.

F. W. Gay (Public Service Electric and Gas Company, Newark, N. J.): The Sealey–Hodtum curve for evaluating the temperature of a transformer at any time in a known-load cycle is a useful and valuable contribution to the art.

Specifications for new transformers should be so written that, for the larger sizes of transformers, a Sealey–Hodtum curve must be supplied for each transformer electrical specification.

In 1913 I made tests on the deterioration of paper insulation at high temperature. These tests covered the temperature range from 140 to 160 degrees centigrade. It was found that if the best type of paper was vacuum-dried before being subjected to 140 degrees centigrade for 24 hours, no appreciable deterioration occurred. However, at 145 degrees centigrade the depreciation was appreciable, and at 160 degrees centigrade, destruction was very rapid.

While it undoubtedly is true that a 24-hour run, at near but less than 140 degrees centigrade, will shorten the expected (many years of) life of a transformer perhaps as much as several months because of insulation deterioration, the probability of a breakdown during this 24-hour period resulting from deterioration caused by heat is probably remote.

There is another risk associated with running transformers at such a high temperature, namely, the risk produced by the possibility of the expansion of the copper (from heat) mechanically damaging the insulation, or developing a fault at a weak spot. The presence of this risk is often expressed by a tester in starting a heat run on a machine in this wise. "If the machine doesn't break down in the first hour, it will run through the test." In the dielectric testing of machines, the same feeling is often expressed in the statement, "If the machine doesn't break down in the first five seconds, it will stand the one minute test." While these expressions are not 100 per cent true, the probability of any failure occurring during an emergency run of, say, 24 hours at heavy overload, is probably greatest in the first half hour. In other words, if the transformer does not break down in the first half hour at any overload giving a hot spot near but less than 140 degrees centigrade, it probably will run through a 24-hour period. A study of the publications on the general subject of overloading transformers would lead an inexperienced engineer to believe that the risk of breakdown on, say, a 24-hour run on emergency overload becomes progressively greater with time.

It is my opinion, that the value 125 degrees centigrade should be set as a hot-spot copper ceiling for the emergency operation of transformers for one hour, or continuously for 24 hours, and that operation at 125 degrees centigrade hot-spot copper, or lower, should be considered as standard emergency operation. Operation at over 125 degrees centigrade hot-spot copper at any time, or near 125 degrees centigrade for more than 40 hours in any five-year period, should be considered as super-emergency.

Operators should be given to understand that any emergency operation above a hot-spot copper temperature of 125 degrees centigrade has associated with it a risk that some weak point will develop in the winding in the first half hour of operation, resulting from the expansion of the conductors, that this risk becomes progressively greater as the hot-spot copper temperature increases above 125 degrees centigrade, and that it almost may be considered as imminent for hot-spot copper temperatures much over 140 degrees centigrade.

It is well known that the connections between coils and between coil ends, tap changers and terminals, as well as bushings and bushing connectors are weak points in a transformer, and are subject to failure as well as the windings; and that the development of a fault at some one of these points in the first half hour of operation probably is as likely to occur as in the winding itself. It is probably equally true that if these connections, and so forth, stand up for the first half hour of operation under an emergency loading, they probably will stand up for the next 24.

V. M. Montsinger (General Electric Company, Pittsfield, Mass.): The method used by the authors to show the heating and cooling copper temperatures can be followed easily, and saves time. However, it should be remembered that one set of curves applies only to a transformer having a given set of thermal characteristics, such as top-oil rise, hot-spot rise over top oil rise, ratio of copper to iron losses, and oil time constant.

While an infinite number of combinations of these characteristics can be built up, yet, as the authors state, results close enough for practical purposes can be obtained with a few sets of combinations. They imply that the two sets as covered in Figures 3 and 5 of the paper can be used in all cases. It is my opinion that at least five or six sets would be required to cover the entire range of transformer characteristics.

The second point is in reference to the time assumed for the maximum temperature during a short-time overload when estimating the effect of the overload on the life of the transformer.

Apparently the authors have assumed that the maximum hot-spot temperature exists during the entire duration of the overload, and if so, the calculated percentage of life used up will be more than is actually used up. Under some conditions the difference may be appreciable.

I would like to ask the authors if they have made any comparison to determine the difference when:

1. Assuming that the hot-spot temperature exists during entire time of overload, in calculating the aging units.

2. Integrating the time-temperature area to obtain the actual aging units.

W. C. Sealey and **J. B. Hodtum:** Mr. Sels has described a means of using the method of equivalent aging for determining permissible overloading of transformers. The curves of the paper will aid in making such calculations. As he points out, planning for emergency overloads must be done in advance before the necessity for the overload occurs.

Mr. Gay's comments are supported by the fundamental principles of insulation deterioration. The rate of deterioration is most rapid near the beginning of the run and the rate progressively decreases as the run is continued. It is always good practice to be conservative in values of allowable hot-spot copper temperature in order to obtain longer life of equipment. The method of equivalent aging is convenient for determining whether temperatures and times being considered are reasonably safe.

With reference to Mr. Montsinger's comments, we are in agreement that results close enough for practical purposes can be obtained with a few sets of curves, but whether two sets are sufficient or more sets are needed depends upon the degree of precision required. The number of curves used should be co-ordinated so that it corresponds to the number of sets of overload values used in the American Standards Association guides for overloading transformers.

As stated in the paper, "For convenience in calculation it may be assumed that the ultimate temperature is maintained for the total time the load remains constant. (If more precise values are desired, the curve of temperature versus time may be drawn.)" In most cases the degree of precision involved in heating and loss-of-life calculations does not justify the more accurate method of drawing a curve and, consequently, the comparison to determine the difference in aging for the two conditions he suggests has not been made. Such calculations could be made easily if a case arose where the precision obtained was justified by the data.

The interest shown in these charts is a gratifying indication that charts of this kind can be useful in overloading transformers.

Asymmetrical Loading of Three-Phase Three-Winding Transformer Banks

Discussion of paper 44-6 by P. K. Denissov, presented at the AIEE winter technical meeting, New York, N. Y., January 24–28, 1944, and published in AIEE TRANSACTIONS, 1944, January section, pages 27–9.

W. C. Sealey (Allis-Chalmers Manufacturing Company, Milwaukee, Wis.): This paper is interesting, because it demonstrates the similarity of problems and their solutions by engineers, regardless of geographical location. Asymmetrical loading of three-phase three-winding transformer banks in which a single-phase load is superimposed on a three-phase load does not present too common a problem in the United States. However, the author solves this problem in exactly the same manner using exactly the same equations as similar problems have been solved in this country. The equations used are familiar.

The solution emphasizes the smallness of the world and its essential unity. An interchange of engineering papers such as illustrated by this one is a sign of the possibility of world unity.

H. C. Louis (Consolidated Gas, Electric, Light, and Power Company, Baltimore, Md.): In connection with the problems associated with carrying unbalanced loads on three-phase transformers, it is practical to mention the effects of single-phase loads on the generators supplying these transformers. It usually is well understood that the capacity of a three-phase generator for carrying single-phase load is definitely limited by the pulsating effects of single-phase loads, consequent double-frequency currents, and heating in the rotor.

It is possible, by using special transformer connections, to divide to some extent load currents resulting from single-phase load among the other phases of the transformer, transmission lines, and generators. Some beneficial effects with regard to regulation, heating, and better balancing of currents and losses may be accomplished in the transformers and transmission lines. On the other hand, regardless of the consequent distribution of the currents in the phase windings of the generators, the net results on the generator are such as still to include the bad pulsating and rotor-heating effects associated with single-phase loading previously mentioned.

Although this principle is not a new one, it is not generally as well known as some other fundamental principles. Accordingly, it is well to remind operators supplying heavy single-phase loads that, although transformer conditions of regulation, loading, and heating may be satisfactory, the evil effects of unbalanced loads on three-phase generators are not eliminated by special transformer connections. The single-phase load limitations of the generators are not changed.

Thermal Protection of Transformers Under Overload Conditions

Discussion and authors' closure of paper 44-65 by V. M. Montsinger and G. Camilli, presented at the AIEE winter technical meeting, New York, N. Y., January 24–28, 1944, and published in AIEE TRANSACTIONS, 1944, April section, pages 160–4.

J. K. Hodnette (Westinghouse Electric and Manufacturing Company, Sharon, Pa.): It has been recognized for a long time that electrical apparatus has considerable latent overload capacity and this is especially true of transformers. Transformer load factors are rarely 100 per cent and ambient temperatures are seldom continuously 40 degrees centigrade. Because of these factors, the name-plate rating of the transformer is often much less than the available capacity for carrying load on a safe thermal basis. Many papers have been presented before the AIEE in an effort to tap this huge reservoir of extra transformer capacity. The rules and guides formulated by these papers have failed to make it possible to use this extra transformer capacity in many instances because of the complexity of transformer heating cycles and the rapid variation in load, which is characteristic of most transformer installations. A properly designed thermal relay is the answer to this problem because it automatically integrates the effects of loads which vary both in duration and magnitude and of changing ambients and in a very simple manner permits operators to interpret these factors in terms of safe transformer loading.

It is indeed gratifying to those of us who have been proponents of loading of transformers on a thermal basis to see this further support and recognition of this idea by Montsinger and Camilli. In a paper presented before the AIEE in 1939 by Putman and Dann on "Loading Transformers by Copper Temperature," the following prediction was made: "The advantages of operation by copper temperature are so obvious that one might predict its almost universal use in the not distant future." It is interesting to note that by the end of 1941, more than one million kilovolt-amperes of power transformers and over 300,000 distribution transformers had been equipped with thermal relays for operation by copper temperature. This principle has been stimulated by the war restrictions and has become more widely accepted.

The relay described by the authors is intended to perform the same functions as the thermal copper-temperature relay which has been used generally in the past. There are a number of differences in the method of operation of these two relays. The major difference arises from the fact that the authors' relay makes use of an auxiliary thermal system having a longer time constant than the transformer itself in order to arrive at a calibration which approximates the recommended loadings in the interim report, while the bimetal-type thermal relay directly employs the transformer thermal characteristics and is adjusted to follow the hot-spot temperature of the individual transformer windings to which it is applied.

The relay described by the authors, which is almost identical to the well-known hot-spot indicator in its characteristics, does not respond quickly enough to short-time overloads to protect the transformer against excessive temperatures. This is due to the difference in time constant of the two systems and the slow response of the thermal element in the relay and is a fundamental characteristic of this type of system. Reference to the authors' Table I shows this effect even at one- and two-hour loadings following no load especially at zero ambient temperatures. For example, the American Standards Association recommended load at zero ambient temperature following no load for one hour is 190 per cent, whereas the relay permits 260 per cent load. By using an element directly responsive to both oil temperature and changes in load current without intermediate thermal steps, the dangerous time lag for heavy short-time overloads is eliminated. Making use of this characteristic, the bimetal-type thermal relay can discriminate between useful overloads and short circuits and thus protect the transformer against dangerous temperatures under all conditions of loading.

The authors have stated in the paper that the direct-operating thermal relay does not give correct results if the ambient changes. It is a fundamental characteristic of the direct-heated bimetal-type relay to tend to compensate for ambient temperature because at lower ambient temperatures the transformer can carry higher loads and in turn more current is required to heat the bimetal to a given temperature. As an example, a five-kva 7,200-volt distribution transformer equipped with a thermally controlled breaker has a maximum variation in copper rise due to ambient temperatures of only five degrees centigrade, either following full load or following no load in changing from 30 degrees centigrade to 0 degrees centigrade ambient temperatures. United States patent 2,320,929 describes a relay for obtaining accurate temperature compensation without sacrificing overload capacity of the transformer.

M. B. Mallett (English Electric Company of Canada, Limited, St. Catharines, Ont., Canada): The thermal protective equipment described in this paper represents one of the tangible results of present efforts to secure maximum apparatus utilization of the power transformer. In regard to the general problem of loading by temperature, it appears to the writer that one of the most important considerations is never sufficiently emphasized—namely, accidental destruction of the machine by abuse is far more disastrous and inexcusable now than in normal times. With this viewpoint as a premise, it follows that a high degree of measuring accuracy for all possible conditions must be unquestionably maintained by the loading gauge.

It is suggested that appreciable horizontal temperature gradients may exist in the top oil of core-type transformers under certain conditions, and that such gradients might adversely affect the accuracy of thermal protective equipment unless given proper consideration. Referring to Figure 1 of this discussion, the arrows indicate the radial flow of oil through the end insulation of a typical core-type transformer. This radial flow ordinarily carries away a large proportion of the total watts generated, the remainder of the heat being taken directly upward by a relatively restricted flow in the core ducts and along the outer surfaces of the core. Assuming a stable initial condition, it appears that a sudden increase in load would tend to create a temporary top-oil gradient between two points such as *A* and *B*, that is the temperature at *B* would rise appreciably faster than at *A*. It would seem that this condition would be more pronounced in very large machines, where the weight of oil above the core may be of the order of 15 tons, and the tank may be some 20 feet in length. Also the presence of relatively deep reinforcing ribs on the under side of the cover might influence the picture.

In view of the above, it would seem that careful consideration should be given in the physical layout, to insure that under all conditions the thermal well is immersed in the *hottest* oil. This might be readily attained in many cases by keeping the thermal well near the tank wall, or possibly under certain circumstances it might be desirable to use a directing tube, as indicated by phantom lines in Figure 1.

It is suggested, that unless the above precautions are taken, the additional variable of horizontal oil gradients may complicate the thermal picture to such an extent as to preclude attainment of the required degree of accuracy.

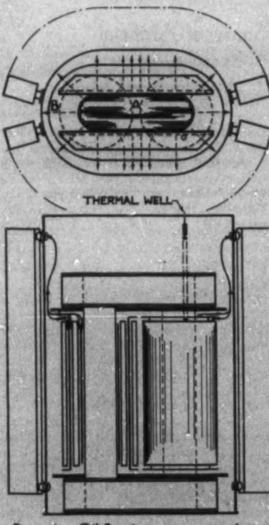


Figure 1. Oil flow in core-type transformer

V. M. Montsinger and **G. Camilli**: In connection with Mr. Hodnette's discussion, it may be of interest to note that a thermal relay similar to that described in the present paper was first produced in 1935 and has been applied to hundreds of transformers since that time. The relay was similar to the device described in the paper in that it employed a thermal element lagged considerably more than the thermal element for the standard winding-temperature indicator, and was equipped with three switches to perform the same three functions as the new relay. However, when the AIEE "Interim Report on Guides for Overloading Transformers and Voltage Regulators" was published it became necessary to incorporate several improvements in the relay to enable it to permit the very much greater short-time overloads allowed in the report. These improvements consisted roughly of (1) providing more lagging on thermal element, (2) setting the thermal element at a lower temperature than the hot spot, and (3) introducing an ambient correction in the relay.

Mr. Hodnette claims that a bimetal-type thermal relay adjusted to follow the hot-spot temperature of the transformer winding will eliminate "the dangerous time lag" for short-time heavy overloads and discriminate between "useful overloads and short circuits." We would like to point out that a bimetal-type relay (which operates at a fixed temperature) adjusted to follow the hot-spot temperature will operate at the same temperature regardless of the time interval involved; therefore, such a relay will not permit a higher hot-spot temperature for a short time as allowed in the "Interim Report" or, if set for a higher-than-normal hot-spot temperature, will allow that higher temperature continuously. Mr. Hodnette is in agreement with this statement because the last half of the second paragraph and the first half of the third paragraph page 1 of his patent, number 2,320,929, states ". . . . so that the thermally responsive device (usually a bimetal strip) would be cooled, by the oil, at the same rate as the copper of the windings, so that the temperature of the bimetal member would match the copper temperature at all times, regardless of the magnitude or the duration of the load, and throughout the entire range of permissible loads."

"The first completely self-protected transformer, when designed as originally outlined, suffered a handicap by reason of the fact that the bimetal strip would theoretically trip out the circuit breaker whenever the bimetal member reached its predetermined temperature for which it was set, which meant that the transformer would be taken out of service whenever the copper, and hence the insulation surrounding the copper, reached this same predetermined temperature, regardless of the length of time during which the insulation had been subjected to that temperature."

The patent states that to overcome this condition it is necessary to set the relay at a temperature lower than the hot-spot temperature and employ an ambient correction. This is identical in principle to one of the methods described in the paper and, in effect, is essentially the same as lagging the thermal element since it results in the relay temperature increasing at a slower rate than the hot-spot temperature.

In the above-mentioned patent it is claimed that ambient compensation is obtained by utilizing a current transformer which saturates at 2.5 or 3.0 times normal current—thereby reducing the current input to the thermal element. The effect of the saturation is to allow greater overloads. However, as stated in our paper and in the Hodnette patent, a scheme using a thermal element adjusted for a temperature lower than the hot-spot temperature and calibrated to give correct results in summer ambient temperatures inherently allows too great overloads in winter ambient temperatures. Therefore, the need is to reduce the overload and not to increase it. In our relay this is accomplished by mounting the controlling switch on a bimetallic element which moves the switch closer to the bellows in cold ambient temperatures thus allowing it to operate sooner.

The use of a current transformer which saturates at 2.5 or 3.0 times normal current also slows up (if not prevents) the relay from protecting the transformer under short-circuit conditions, which is contrary to the claim in Mr. Hodnette's discussion.

Mr. Hodnette claims that there is a "dangerous time-lag for heavy short-time overloads" in our lagged thermal element and to support his claim he points out that in Table I where the ASA recommended load at zero ambient temperature for one hour is 190 per cent the relay permits 260 per cent load. This difference is not due to the time-lag of the thermal element—as shown by the close agreement between the overloads permitted by the relay and those recommended in the AIEE Interim Report where (if affected by time lag) the difference would of course be greater on account of the greater overloads involved. The two columns of "Load Permitted by Relay" in Table I are the values at which the signal and alarm contacts operate; that is, the signal contact operates when the loads under "ASA Emergency" heading are reached, and the alarm contact operates when the loads under "AIEE Emergency" heading are reached. If desired, the relay can be calibrated to give the alarm at the ASA overloads with an accuracy comparable to that now shown for the AIEE overloads.

Mr. Mallett mentions the possibility of the heating coil being away from a stream of localized hot oil coming upward under a short-time heavy overload, and wonders if it would be desirable to use a tube to direct the hot oil upward and around the heating coil as indicated in his sketch. In all our experience in conducting short-time heavy overloads, we have not discovered any serious horizontal oil temperature gradients within the region over the core and windings. Several years ago an appreciable gradient was found from one end of a long oval tank to the other end. The reason for this was that the core and windings were not symmetrically placed in the tank, being much nearer one end than the other end. The oil immediately over the core and winding was several degrees higher than it was at the other end of the tank.

If the heating coil is placed in the oil over the core and windings, we do not believe that there is any danger of serious horizontal oil temperature gradients being set up even under overload conditions.

Study of Artificial Respiration on Anesthetized Men

Discussion and authors' closure of paper 44-22 by W. B. Kouwenhoven, D. R. Hooker, and J. A. York, presented at the AIEE winter technical meeting, New York, N. Y., January 24–28, 1944, and published in AIEE TRANSACTIONS, 1944, January section, pages 1–2.

B. L. Vosburgh (nonmember; General Electric Company, Schenectady, N. Y.): According to Burton Opitz' physiology a full-grown man inspires and expires about 500 cubic centimeters of air with each respiratory act. This figure compares favorably with the average of the 15 subjects in the prone position, namely 545 cubic centimeters and 590 cubic centimeters for subjects *A*, *B*, and *C*. When anesthetized this tidal air volume dropped to 264 cubic centimeters average with the minute volume at 4,752 cubic centimeters compared to 7,670 cubic centimeters in the wakeful state which is a decrease of about 60 per cent per respiration and 30 per cent per minute volume. This remarkable difference holds for all comparisons between the conscious and anesthetized state and must mean a lessened metabolic demand in the latter. It is quite likely then that 350 to 400 cubic centimeters of air would meet adequately the anesthetized subjects needs for oxygen. Instead we note 590 for the original Schafer method, 746 for the modified Schafer, and 799 for the pole-top method. These figures represent a surplus air intake of from 60 to 100 per cent beyond metabolic requirements. This means hyper-ventilation with consequent lowering of the blood carbon dioxide, which, of course, depresses central stimulation of respiration. This effect might well tend to prolong a paralysis of respiratory function.

Perhaps the lesson to be learned from this very enlightening study is that all methods of artificial respiration are too efficient to be most effective in stimulating the respiratory center. It would be interesting to repeat the observations with carbon-dioxide blood determinations in order to find out whether or not the administration of carbon dioxide along with artificial respiration is indicated.

C. F. Engel (nonmember; Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The thought has been in my mind for several years, the result of a conference with W. B. Kouwenhoven, that possibly we might need some new form of artificial respiration. The success of the pole-top method in my mind cannot be wholly attributed to the saving of time in removing the victim from the top of the pole to the ground. I believe that the wrapping of the arms about the patient's abdomen is not only an excellent form of artificial respiration, in that it causes a better ventilation of the lungs than the Schafer method, but it also very materially assists in reviving the victim by forcing the blood out of the abdomen either mechanically or by stimulating the vaso constrictor nerves. The theories of asphyxiation from electric shock include a fibrillating heart and it is my opinion that in forcing the blood from the abdomen very materially aids in preventing fibrillation.

It has been said by someone, that a man can bleed to death in his own blood vessels without shedding a drop of blood. This, of course, means that there is an accumulation of blood in the abdomen and that other parts of the body suffer from a lack of blood. This is definitely proved by well-known methods of reviving a person from a faint, by either doubling them up or using pressure on the abdomen. I believe that in some way electric shock and fainting can be tied together, at least no form of artificial respiration which does not stimulate the circulation could be of much value. In other words, we could keep a man breathing for a long time by ventilating the lungs, but if circulation was not started, we could not hope for ultimate recovery. At least the man would not take up the job for himself unless the blood carrying the respiratory stimulant, carbon dioxide, was carried to the respiratory center in the brain.

W. B. Kouwenhoven, D. R. Hooker, and **J. A. York:** Doctor Vosburgh's discussion is very interesting and points to the fact that there is need for more research in order to understand the artificial-respiration problem.

Barcroft in England showed many years ago that hyperventilation of the lungs leads to respiratory rest, which continues until carbon dioxide and other acid stimulants accumulate in the tissues and blood stream in sufficient amount to arouse activity in the respiratory center. Doctor Yandell Henderson, in the United States, has shown that the addition of one half per cent of carbon dioxide to the air inspired in artificial respiration appreciably facilitates the return of normal breathing. It is for this reason that students of the problem oppose the use of mechanical respirators, particularly in the hands of untrained operators.

In our research we were primarily concerned with the evaluation of the efficacy of the methods of artificial respiration and were particularly interested in the pole-top method as compared to other methods. We, therefore, limited our tests on anesthetized subjects to three methods of artificial respiration: the standard Schafer, the modified Schafer, and the pole-top method. Anesthetized subjects were used to meet the objection raised by some authorities that conscious subjects might be overco-operative.

It would be extremely interesting, as Doctor Vosburgh suggests, to carry out a laboratory experiment on the utility value of supplementing the air drawn into the lungs with carbon dioxide. This would certainly add to our present knowledge. Even if the addition of the carbon dioxide proved helpful it is doubtful if it would be possible to supply lifeguards and emergency crews with the required equipment for field use.

We do not believe that there are valid grounds for regarding any of the present methods as too efficient. Excess oxygen in the air breathed at normal atmospheric pressure does no harm. A depleted supply, on the other hand, is extremely deleterious. In emergencies when normal breathing has stopped the first requisite is to provide the tissues and more particularly the tissues of the brain and nervous system with oxygen.

With reference to the discussion by Doctor C. F. Engel, we believe that the compression of the abdomen that takes place in the application of the pole-top method aids in promoting the circulation of the blood. We do not know, however, that the pole-top method has any effect on the fibrillating heart.

The Geometry of Arc Interruption—II
Current-Zero Phenomena

Discussion and author's closure of paper 44-44 by E. W. Boehne, presented at the AIEE winter national technical meeting, New York N. Y., January 24–28, 1944, and published in AIEE TRANSACTIONS, 1944, pages 375–87.

H. A. Peterson and **T. W. Schroeder** (General Electric Company, Schenectady, N. Y.): The author presents a sound basis for evaluating the recovery-voltage transients for circuit interrupters in circuits having up to two natural frequencies of oscillation. The method employed rests on the firm foundation afforded by an intimate understanding of the principle of superposition. Its chief limitation from the standpoint of general usefulness lies in the fact that the arcing characteristics of a given interrupter must be known before it can be applied. Thus, a certain number of actual test or empirical data is prerequisite.

It is shown clearly that an interrupter having control of the current-zero instant, thus rendering circuit constants of little consequence, can be very effective in reducing the severity of the voltage-recovery transient. For this type of interrupter the number of natural frequencies characteristic of the circuit interrupted should be of little importance.

One of the aspects of circuit interruption most difficult to analyze is that associated with current discontinuities during the arcing period. The arc restriking and reclearing in certain sequences frequently can give rise to recovery and system overvoltages far more severe than those associated with a single interruption. The author's treatment is concerned only with an interrupter's ability to clear the circuit initially. In many cases, particularly for high-current interruption, this is the most important consideration. However, in numerous cases, particularly for lower currents, arc restriking can lead to overvoltages which are much more severe than those associated with the initial interruption. These overvoltages are dependent in general on both the circuit constants and the interrupter characteristics. Such cases can be studied most efficiently by means of miniature systems and synchronous switches. This approach to the restriking overvoltage problem necessarily requires assumptions regarding the interrupter's arcing characteristics, just as the author's approach to the initial interruption problem requires such assumptions.

T. E. Browne, Jr. (Westinghouse Electric and Manufacturing Company, Trafford, Pa.): In general, the current-zero phenomena long have been recognized to be of essential importance in arc interruption of alternating currents, and so they deserve and have received a great deal of study. The admitted difficulties in making complete measurements of the very short-lived quantities involved make a careful and clear analysis of these phenomena all the more desirable.

Mr. Boehne has contributed an interesting and ingenious method for computing the transient voltage appearing in this current-zero time region. Since this voltage is the one quantity which can be measured easily with a cathode-ray oscillograph, it is, of course, not the result itself, but the basis for this computation which is of the most interest. That is, we are concerned with the effect on the recovery voltage of the various associated and initial conditions, including the rate of current approach to zero and also the related behavior of the arc resistance near current zero. The operational method used has considerable mathematical elegance but suffers from the use of a whole array of unfamiliar and generally artificial geometrical parameters which, I believe, serve to conceal rather than to clarify the physical phenomena underlying the calculations. Also, it seems to me that the somewhat unusual view of the current rather than the voltage as the independent causative variable tends to lead to confusion in thinking.

Mr. Boehne mentions certain computed actual current variations which suggested his assumed types of current diversion, but unfortunately he does not show any of them. Therefore, it may be useful to call attention to some samples of such computed current diversions presented by Van Sickle and Berkey about 11 years ago in a paper[1] not included in Mr. Boehne's bibliography. These do show variations of both the high-rate and low-rate types for oil circuit breakers which are in good agreement with Mr. Boehne's assumptions up to current zero. At variance with Mr. Boehne, however, they also show, for the low-rate cases, continuity of arc current and of arc resistance through current zero and well into the voltage-recovery region beyond. This departure of Mr. Boehne's mathematical assumptions from the logical and demonstrated physical behavior of the arc seems to me to be the most serious fault in his calculations, but one which probably could be corrected without departing from his method.

Van Sickle and Berkey also illustrated two additional phenomena, implicit in Mr. Boehne's assumptions and calculations but not mentioned by him, which should aid in clarifying the basis for his assumed current diversions. The first of these is the dynamic volt-ampere characteristic of the arc, which determines by its rising or falling trend whether the current follows the high-rate or low-rate type of diversion. The second and co-operating factor is the charging current through the shunting capacitance of the breaker and its leads which, being proportional to the rate of change of the arc voltage, makes the discontinuity in the rate of arc-current decay possible without a corresponding voltage discontinuity. This also accounts for the nonsimultaneous passage through zero of the voltage and of the total current. It appears to be failure to recognize this latter point which leads to most of the confusion in Mr. Boehne's discussion of arc-gap-resistance variations in connection with Figures 10 and 12 of his paper. As Van Sickle and Berkey correctly showed, it is the arc current which reverses with the arc voltage and not the total current, since the latter includes the capacitive current component which is not zero at the moment the voltage passes through zero. Any shunting resistance, as shown in Figure 10, would contribute a diversion of current from the arc, but this current component necessarily would be in phase completely with the voltage. This clears up the contradiction of having the arc current in the low-rate case go through zero with the voltage in Figure 12 but not in Figure 10!

In attempting to understand Mr. Boehne's remarks about "incorporating desirable current-zero characteristics" in design, it should be helpful to keep in mind the simple fact that a circuit breaker can act on an arc in only two basic ways:

1. It can lengthen it.
2. It can subject it to deionizing influences.

The modern trend in exclusively a-c circuit breakers has been toward great intensification of the deionizing influences, especially at current zero, while keeping the arc as short as possible. Such breakers have attained very high interrupting efficiency; that is, they are able to interrupt large amounts of power with a minimum of energy absorption. That such breakers fail to modify the circuit voltage-recovery transient in a way that is favorable to them by no means prevents them from being effective. In contrast are the useful but inefficient breakers of the magnetic-blast or magnetic-deion type with which Mr. Boehne is especially concerned. These are essentially d-c-type breakers used to interrupt alternating current. In addition to lengthening the arc, the breakers exert powerful deionizing forces on it while the current is high, which results in very high arc voltages and consequent energy absorption, but this deionizing activity practically ceases as current zero is approached. The weak deionizing action near current zero usually does result in a smoothly falling arc voltage and consequently a low-rate type of current diversion with its associated low arc resistance at and just after current zero. The oscillograms clearly show, however, that this effect alone is not sufficient to prevent reignition of the arc until the increasing arc voltage has drastically reduced the current magnitude and so shifted its phase that voltage recovery can occur only at an extremely low rate. Under these conditions the arc finally becomes unstable and "goes out," even as a d-c arc would. Thus, it may be seen that this type of breaker is exceptional in that its effectiveness depends primarily on what it does to the arc between current reversals, rendering the usually paramount current-zero phenomena of secondary and even minor importance in this particular case. In view of this, one wonders whether the designer of a magnetic-blast or a magnetic-blowout type of breaker has any real need to consider current-zero phenomena at all!

REFERENCE

1. ARC-EXTINCTION PHENOMENA IN HIGH-VOLTAGE CIRCUIT BREAKERS—STUDIED WITH A CATHODE-RAY OSCILLOGRAPH, R. C. Van Sickle, W. E. Berkey. AIEE TRANSACTIONS, volume 52, 1933, pages 850–7.

Robert H. Nau (captain, Signal Corps, United States Army, Fort Monmouth, N. J.): Again, in the present paper, which is one of a series of papers presented by Mr. Boehne on the subject of geometry of arc interruption, there is no allusion or reference to the works of the renowned research engineer—Joseph Slepian; neither is there reference to the papers by Ludwig, Nau, and Dickinson which describe in detail the theory of arc extinction as utilized in the functional operation of a complete line of air circuit breakers known as magnetic-deion breakers. This seems somewhat paradoxical when the test data of these breakers are compared with that given by Linde, Wyman, and Boehne in papers presenting the story of the "Magne-Blast" breakers.

It would be particularly interesting to observe and to study complete series of single-phase interruption tests conducted on the Magne-Blast breaker with short-circuit currents ranging to a reasonable percentage of that of the short-circuit interrupting rating and having at least 86 per cent of the rated three-phase voltage impressed across the single pole. It is reasonable to expect such short-circuit conditions in field operation.

Mr. Boehne is to be commended on his present treatise, "The Geometry of Arc Interruption," if for no other reason than because he does not use the lemon-shaped voltage-current trace in which the former paper on the same subject delved.

E. W. Boehne: Peterson and Schroeder have raised some very interesting questions regarding the application of the relations developed in the paper. Although the method of attack was described for circuits having one or two natural frequencies, this by no means limits the solution to such circuits. Analytically, the method is adaptable to circuits having any number of natural frequencies. In the solution of recovery-voltage phenomena, the vast majority of circuits can be expressed accurately by not more than two natural frequencies. It was for this reason that the presentation was restricted to such circuits. For design problems outside the field of circuit interruption, the application or the method to circuits having three of more natural frequencies might prove more practical.

The chief value of the method and relations developed lies in the study of circuit-interrupter performance from a design viewpoint to achieve a clearer working understanding of the phenomena associated with interruption and, in particular, to reveal and encourage the manner in which the circuit interrupter can be made to influence beneficially the recovery voltage. In this manner, studies of the type presented lead to improved designs which control "the arcing characteristics of a given interrupter," which Peterson and Schroeder quite correctly state "must be known before they can be applied." The limitation to which they refer is present from the immediate field-application viewpoint. Test data always will be desirable prior to the field application of any equipment. It is the basic trend in the present paper to develop interrupters which have as favorable "test characteristics" as possible. In the long run, it is believed that the limitation of immediate field application will be overshadowed by the creation of interrupter characteristics desirable for wider field application. The second paragraph of their discussion reveals that they share this viewpoint.

Peterson and Schroeder quite properly emphasize the importance of restriking phenomena as the probable cause of the majority of system overvoltages due to switching. Although it is outside the scope of the present paper, it seems appropriate to point out that, in some cases, the overvoltages precipitated by a single off-current-zero interruption might form the trigger action which gives rise to some restriking phenomena. Other types of restriking phenomena which normally are independent of the interrupter's ability, however, can conceivably become aggravated by the interrupter. The control of the basic restriking phenomena lies outside the ability of the interrupter alone but depends principally on the correct circuital environment of the interrupter.

Previous studies made of overvoltages caused by restriking show that the phenomena are possible only when the high-frequency current associated with the recovery-voltage oscillation passes through zero. Circuit criteria have been set up which make it possible to determine the correct ratios of circuit parameters to avoid this prerequisite of restriking phenomena. Such analysis, however, has been predicated on normal current-zero interruptions. When interruptions occur in the high-rate region, as described in the paper (Figure 1), the associated high-frequency currents have larger amplitudes and may be forced through zero under conditions which otherwise would have been considered harmless. In this manner, the characteristics of the interrupter, particularly for lower currents, can extend the range of external circuit constants which give rise to the possibility of multiple restriking within the interrupter—a recognized source of overvoltage. This problem requires additional study.

Conversely, interrupters which perform consistently in the low-rate region (Figure 1 of the present paper) give rise to milder voltage and current oscillations and accordingly reduce the range of external circuit constants which can give rise to the restriking phenomena. This fact has been demonstrated by the test and field performance of the Magne-Blast air circuit breaker which performs almost entirely in the low-rate region.

In summarizing these viewpoints, therefore, it seems that the subject paper, although confined to the interrupter's ability to clear the circuit initially, offers the opportunity to add another dimension to the analysis of the problem of circuit-interrupter restriking by incorporating the breadth of observed current-zero phenomena.

Doctor Browne's constructive discussion is appreciated. He has raised several points which will be taken up in the order in which they are presented.

The majority of circuit problems has as the driving force a set of voltages with the various currents as the objective. Kirchhoff's and Ohm's laws normally are presented in this manner. Hence, when we encounter problems in which a voltage is being sought, it is quite correct that the current be considered as the independent variable. Ohm's and Kirchhoff's law are just as adaptable to this condition as the more normal problem. Doctor Browne is of the opinion that "the somewhat unusual view of the current rather than the voltage as the independent causative variable tends to lead to confusion in thinking." The author is of the opposite opinion and presents for comparison the alternate methods of solving the problem:

(*a*). When a recovery-voltage trace is given, together with the circuit, an analysis is possible which becomes quite involved because of the nonlinearity of the resistance parameter. This requires a step-by-step solution or the counterpart in the analysis of a given film. Such a procedure is tedious and requires extremely clear and orderly thinking to reach a correct solution. When completed, the result is valid only for the case considered.

(*b*). Classical linear differential equations will give the correct answer on the basis of an assumed current path. Once the equations have been set up with the proper boundary conditions, all touch with the mechanism of the phenomena is lost, except that the correct answer comes out in the solution. When applied to the simplest case of vertical current cutoff, the results are as determined and given in reference 4 of the paper and repeated in equation 5 of the paper. When extended to the more general case of linear-current diversion the boundary conditions become so involved, according to the writer, as "to lead to confusion in thinking." General and useful results would be obtained if ever solved by this method.

(*c*). When the aforementioned general and useful results were sought in the simplest manner possible, the straightforward principles of superposition were employed. These consist of resolving the cancellation current into two components: a d-c component (termed the rectangular component to avoid confusing with the d-c component of asymmetrical short-circuit currents) and a linearly rising current, commed the triangular component. By determining once and for all the "building-block" voltages produced in standard recovery-voltage circuits when these two components of the cancellation current are injected (equations 3 and 4 of the paper), makes possible the fabrication of the recovery voltage to conform to any type of simple or compound linear-current diversion desired. Compact formulas, which express these general results, have been determined once and for all. The method is rigorous and reveals the mechanism of the overvoltage and the part played by each variable. To the writer the method employed not only leads to clear thinking and visualization but also useful general results. When used to express the results for a complete family of variations of the independent variable, it becomes a powerful and novel tool in the hands of the design engineer.

Although the basic method is not new (suggested by Vannevar Bush, reference 5 of the paper), its application to the general recovery-voltage problem gives rise to new and descriptive terms which express in compact form the concepts desired. Their newness should be no handicap. Although Doctor Browne refers to these as "unfamiliar and generally artificial geometrical parameters," he quite generously seems to have accepted the terms, as he uses them throughout the balance of his discussion. He quite correctly calls to the reader's attention the excellent oscillogram analysis of Van Sickle and Berkey some 11 years ago. They exhibit in detail how the particular interrupters behaved and are noteworthy examples of method *a* described in this closure. Also omitted from the reference list (with the author's apology) was the more recent analysis by R. M. Bennett of air-blast-breaker performance which appears in the Appendix A of the paper by D. C. Prince, J. A. Henley, and W. K. Rankin.[1] A thorough search of the literature unquestionably would reveal other examples of this straightforward approach to the interpretation of individual current-zero performances.

A study of the subject paper will reveal that it is not the purpose to describe in detail the specific characteristics of any one particular interruption or to predict the performance of any specific design. Instead, an approach has been made to describe the entire family of current variations in the current-zero region in such a way as to permit the qualitatively minded to come rapidly to an understanding of the basic cause of an observed recovery characteristic and at the same time to crystallize the results in compact formulas to permit the quantitatively minded to work out specific numerical examples which best approximate his observed phenomenon. This approach has been accomplished in a rigorous straightforward manner in terms of the standard nomenclature of circuit theory. Thus free of the static limitations of any particular test performance, the method offers a freedom of choice of the family of current-zero variations but is not restricted to any particular type. The basic variation chosen as a result of oscillographic observation was one in which the currents deviated from the normal linear path to approach zero linearly at a new rate. These currents first were assumed to stop at current zero. Doctor Browne objects to this. Such an objection is well founded and is undoubtedly based on his close attention to the details of specific interrupter characteristic. This objection would have been more valid, however, had the paper ended without giving consideration to the extension of the method to include current variations beyond the normal current zero. This is done in three specific cases and is acknowledged in the final words of his third paragraph. To clear this matter, the three methods presented in the paper for considering the transition from arc current to arc-gap-leakage current are:

1. The direct introduction of this leakage current as suggested in Figure 5c of the paper. This current is shown, in general form, delayed in its occurrence *G′*. Figure 18a of the paper shows an oscillogram exhibiting this general characteristic.

2. As part of the tail end of the exponential decay as shown in Figure 11b of the paper and described in the paragraph under "Other Types of Current-Zero Phenomena."

3. By incorporating the arc-gap-leakage path within the interrupter as part of the resistance assumed shunting the breaker. This is described in the text associated with Figure 10 of the paper.

In the majority of cases, this resistance may be made up entirely of the transient arc-gap-leakage paths which he correctly believes should be incorporated to account for observed phenomena in power circuit breakers of the magnetic type. The reader is referred to the text in connection with Figure 10 (last three paragraphs under "Arc-Gap Resistance").

At this point Doctor Browne refers to "the nonsimultaneous passage through zero of the voltage and the total current." From his discussion which follows this point, it seems clear that he is defining total current to include all the current associated with the capacitance shunting the breaker. Although this capacitance current has been accounted for correctly and automatically, in a strictly rigorous manner in the method of analysis demonstrated in the subject paper, at no point in the presentation was it necessary to consider its presence or to include it in the analysis or diagrams. At only one point in the paper were the words "total current" used, and this is in Figure 10. Here it is very clearly indicated to be only the current associated with the breaker. It would have been made clearer had it been termed "total arc-gap current" on Figure 10 as was done in the text concerning that illustration. It seemed unnecessary prior to this time to state that only that part of the current associated with the breaker is described in the text and in all the diagrams. It is the only current in which we specifically are interested. It is the current that would be measured by the shunt in series with the breaker. Any attempt to go outside the breaker and include the capacitance current, as incorporated in the diagrams presented, will lead to hopeless confusion. In Figure 12, as in Figure 10, the arc voltage must go through zero with the total arc-gap current as shown in both diagrams. This is in agreement with Van Sickle and Berkey, and I might add that it is the only answer consistent with Ohm and Kirchhoff. Since Figure 10 is specifically tied down to a circuit incorporating a resistor (assumed part of the arc path in this case), it can bear considerable study toward a clearer insight of the physical mechanism of interruption.

Doctor Browne points out two basic ways in which a circuit breaker can act on an arc. Our viewpoint becomes broadened, however, when we study how these basic actions alter this new circuit-parameter–arc resistance. In reference 1 of the paper the voltage drop across the arc was shown to be the controlling factor and capable of modulating the energy liberated within the breaker. Figure 1 of this discussion shows the variations in arc energy and peak recovery voltage as a result of the magnitude of arc voltage. These relations are valid for essentially rectangular arc voltages acting on symmetrical short circuits of zero power factor. The recovery-voltage curve shown in Figure 1 of this discussion is computed on the basis of normal current zeros without any benefit of the current-zero phenomena described in the paper. These are the positive factors in which we really are interested, and they define the performance of the interrupter in its environment. These circuital factors certainly should be added to the basic actions of the circuit breaker in achieving interruption.

The "exclusively a-c breakers," to which Doctor Browne refers, function almost exclusively on the left-hand side of the master characteristics of Figure 1 of this discussion. These are the oil and air-blast breakers, and all function in the low-arc-high recovery-voltage region of Figure 1. Maximum arc energy, however, is developed for arc voltages which are only 31 per cent of the generated voltage. An arc voltage of ten per cent will produce energies in excess of half of this peak.

In spite of these proved master characteristics and their associated variations developed in reference 1 of the paper, Browne states that the action of the Magne-Blast breaker results "in very high arc voltages and consequent energy absorption." To the contrary, arc voltages in excess of the 31–35 per cent band (all variations in asymmetry considered) give rise to reduced arc energy and, in addition, function at a

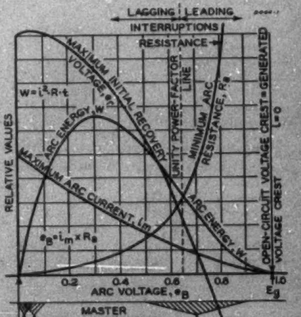


Figure 1. Characteristics of a Magne-Blast circuit breaker

much reduced recovery voltage to which must be added the benefits of the current-zero phenomena of the low-rate type described in the subject paper. An arc voltage of 60 per cent normal crest produces nominally the same arc energy as an arc voltage of ten per cent. Arc-voltage characteristics in excess of 60 per cent are common to the Magne-Blast-breaker experience and permit interruptions in the low-energy leading phase-angle area, as described in numerous papers (references 1, 10, 11). Doctor Browne's description of this performance as "inefficient" seems unfounded by field experience.

The Magne-Blast breaker is one of the best interrupters of direct current encountered by the writer. The right-hand side of the diagram of Figure 1 (this discussion) could well be termed the d-c side of the master characteristic. When applied to alternating current, they form some of the best interrupters known. To Doctor Browne's sentence to the effect that "the arc finally becomes unstable and goes out" should be added "in less than a cycle of arc." Thus, it may be seen that this type of breaker is exceptional in that its effectiveness lies not only in its ability to shift the phase angle of the circuit during a brief arcing period but also to reduce further the remaining recovery voltage impressed on it by the favorable action of a low-rate type of current-zero phenomena.

Although this discussion seems to have centered about circuit-breaker performance and, in particular, the low-rate type of current diversion in magnetic-type air circuit breakers, it should be pointed out anew that the approach to this problem was planned on a much broader field of application. The work is quite general and adaptable to any current-zero phenomenon, whether it occurs in a fuse, furnace, rectifier, or a circuit breaker. It has already found several interesting applications in these fields.

"The lemon-shaped voltage-current trace" to which Captain Nau refers is Figure 1 of reference 1 in the subject paper and is the standard circle diagram of the $R-L$ circuit under steady-state conditions. It was used as a "stepping stone" to the development of the corresponding transient characteristics which are presented in compact form, as Figure 1 of this discussion, and reviewed in connection with Doctor Browne's discussion mentioned previously. This circle diagram was used first quite appropriately (for circuit-breaker studies), in my discussion of L. R. Ludwig's and R. H. Nau's paper,[2] to point out the misconception regarding the relationship of arc voltage to arc energy, namely, "the energy dissipated in such an arc is necessarily high because of the long arc length and consequent high arc voltage."[3] Captain Nau's objections to this diagram are that it is a steady-state diagram, does not apply to the transient phenomena, and might be misinterpreted as being applicable. The transient characteristics now being available leave little excuse for this approximation. It is interesting to note, in spite of the slight differences, that maximum energy occurs with a nominal 45-degree shift in phase angle in both the steady-state and transient case. Captain Nau endeavored to show that these relations do not hold in the transient case by his oscillographic proof (Figure 7 of his closure)[3] in which, by "special means," he succeeded in developing an arc voltage of 20 per cent of the normal voltage crest which resulted in a current suppression to 70 per cent of normal crest and generated an arc energy of about 90 per cent of the crest of the energy characteristic (see Figure 1 of this discussion) with a corresponding phase-angle shift of only 30 degrees. He concludes this investigation by stating, "Thus, it is shown that to suppress current by increasing the arc voltage actually does cause a considerable increase in energy dissipation." A study of the master transient characteristics of Figure 1 of this discussion, however, will show that the two points which he chose lie on the steep left-hand side of the master energy characteristic and, as far as the investigation went, his conclusions seem justified. Had he reduced the generator voltage to continue the investigation, however, he empirically would have reached the complete master characteristic which fits his two experimental points quite accurately. The reasons for operating a magnetic air breaker at high-arc-voltage levels (60 to 80 per cent of generated voltage crest) become appreciated with a study of the true master characteristics and a review of the performance record of the Magne-Blast breaker.

REFERENCES

1. The Cross-Air-Blast Circuit Breaker, D. C. Prince, J. A. Henley, W. K. Rankin. AIEE Transactions, volume 59, 1940, September section, pages 510–17.

2. Discussion by E. W. Boehne of Magnetic Deion Air Breaker for 2,500–5,000 Volts. AIEE Transactions, volume 59, 1940, page 1126.

3. Closure by R. H. Nau of Magnetic Deion Air Breaker for 2,500–5,000 Volts. AIEE Transactions, volume 59, 1940, page 1127.

A 500,000-Kva 7.5-Kv Air Circuit Breaker for Steel-Mill Service

Discussion of paper 44-56 by R. C. Dickinson, presented at the AIEE winter technical meeting, New York, N. Y., January 24–28, 1944, and published in AIEE TRANSACTIONS, 1944, May section, pages 242–5.

L. J. Linde and **B. W. Wyman** (General Electric Company, Philadelphia, Pa.): It has been of much interest to watch the development of the magnetic-type air circuit breaker and its applications to higher-capacity circuits. During the past six years this Institute has witnessed the gradual advancement of these breakers until we now have ratings from 50,000 to 500,000 kva at votage ratings of from 2,300 to 15,000 volts. Mr. Dickinson has presented an air circuit breaker designed to satisfy the very difficult problem of repetitive duty with high-current interrupting capacity. This breaker should be a welcome addition to the growing line of magnetic-type air circuit breakers.

A review of this paper emphasizes the problems involved in obtaining the high-current rating listed for this capacity. Apparently, considerable care has been exercised in providing sufficient ceramic areas for absorbing the energy during the interruption. The refractory plates are separated as much as possible for venting without sacrificing the deionization effect on the exhaust gases. Separated vents are spaced between the stacks of refractory plates. There is apparently no process for cooling exhaust gases through these separate vents. Unless these gases are cooled effectively, it might be possible for incandescent gases to escape through these vents into the switchgear structure. We would appreciate an additional explanation on the treatment of these gases.

Because a breaker of this type should find some of its applications on 60-cycle circuits, we naturally would look for more test information at this frequency. Closing–opening tests at 60 cycles usually are more severe than similar tests at 25 cycles, for the short-circuit currents reach their crest much sooner after the contacts first touch. The resultant magnetic force might possibly reach its peak before the mechanism is latched on the 60-cycle test, whereas, comparable mechanism speeds might latch the breaker before the peak of magnetic forces when making against high currents at 25 cycles.

An examination of Figure 4 of the paper demonstrates the value of ohmic resistance in the power arc, for the second half cycle of arc current is reduced considerably with an increase in arc voltage. The circuit is interrupted near a voltage zero, automatically throttling rate of rise of recovery voltage to an extremely low value. Mr. Boehne's paper "The Geometry of Arc Interruption"[1] and companion papers have emphasized the advantages of just this phenomena.

REFERENCE

1. The Geometry of Arc Interruption—II. Current-Zero Phenomena, E. W. Boehne. AIEE Transactions, volume 63, 1944, pages 375–87.

H. V. Nye (Allis-Chalmers Manufacturing Company, Milwaukee, Wis.): This paper is interesting in that it shows the possibility of producing a breaker of fairly large interrupting capacity without departing a great deal in the design features from smaller breakers which have been in use on service voltages up to five kilovolts.

The expansion and magnification of the arc-chute structure, however, does seem to indicate that this type of breaker will require more space than the equivalent oil-circuit-breaker units. This is evidenced by Figure 7 of the paper which shows a metal-clad unit 9 feet high and 92 inches in depth. Could the author give us any idea of what dimensions might be expected on a 15-kv breaker of the same rating?

The data in Table I of the paper show extremely fast mechanical operation requiring only $1^1/_4$ cycles to part the breaker contacts. The arcing times also look good for a magnetic breaker, but they do not show the consistency on heavy currents that might be expected. It is unfortunate that data were not incorporated to show the performance of this breaker on lighter currents, particularly in the range from 300 to 1,500 amperes. A statement is made that the performance throughout the range was found "satisfactory," but it would have been more convincing to submit some of the test data to show just how good the operation was at lighter currents. This is particularly interesting, as apparently no air booster is employed to help out on these light loads.

A New Three-Element Current-Limiting Power Fuse

Discussion and authors' closure of paper 44-48, by H. L. Rawlins and H. H. Fahnoe, presented at the AIEE winter technical meeting, New York, N. Y., January 24–28, 1944, and published in AIEE TRANSACTIONS, 1944, April section, pages 156–9.

E. A. Williams, Jr. (General Electric Company, Philadelphia, Pa.): It is quite an unusual occurence when two separate groups of engineers working in two different organizations can reach the same conclusions relative to the general application and design requirements for a line of power fuses. A comparison of the Rawlins–Fahnoe paper with the Prince–Williams paper[1] and the Williams–Schuck paper[2] shows a remarkable close agreement on all essential fundamentals.

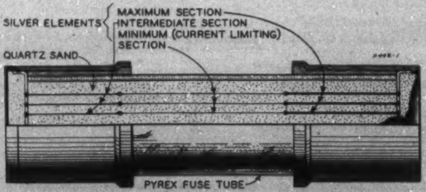


Figure 1. Prince–Williams–Schuck fuse — 2,500- and 5,000-volt design

The three elements of the Rawlins–Fahnoe fuse may be described generally as:

1. The interrupting element.
2. The current-limiting element.
3. The voltage-limiting element.

The fuse originally described by Prince and Williams included the first two elements and the commercial version of the same fuse, later described by Williams and Schuck, included all three elements. Figure 1 of the latter paper shows a wire-wound shunt resistor, or voltage-limiting element, connected in parallel with the conductor wires which form the current-limiting element. Figure 3 of the same paper shows a novel method of combining the functions of the current-limiting element and the voltage-limiting element. The use of the stepped wire shown also provides the "cool operation" of the fusible element shown in Figure 4 of the Rawlins–Fahnoe paper.

In practice, the stepped wire is formed by welding together several silver wires having diameters in the same order of size as those described by Rawlins and Fahnoe. These stepped wires are used in place of the straight wires originally described in the Prince–Williams paper. The step having the smallest diameter forms the current—limiting element, and the larger steps form the voltage—limiting element.

The functional similarity of the two fuses may be seen by comparing the current-limiting characteristic curves, Figure 1 of Rawlins–Fahnoe and Figure 2 of Williams–Schuck. It will be noted that both fuses rated *E* 200 amperes will limit an available current of 80,000 rms amperes to a peak value of approximately 30,000 amperes.

While the functional similarity of the two fuses is very remarkable, the structural differences are even more striking. This fact becomes obvious when the construction of the Rawlins–Fahnoe fuse (Figure 2 of the paper) is compared with the construction of the Prince–Williams–Schuck fuse as shown by Figure 1 of this discussion.

We agree emphatically with the author's conclusions concerning the necessity for testing combination fuse and motor starters to insure a successful over-all application of the combination. However, the authors perhaps overlooked one important consideration. It is essential also to know that the fuse is capable of withstanding without damage the overload current associated with motor starting, reversing, plugging, stalling, and so forth. These operations may occur relatively frequently and may result in cumulative damage to the fuse unless overload relays with carefully selected characteristics are provided. The ideal condition is one in which the relay is a thermal type so designed that under overload conditions its actuating element will heat faster and cool more slowly than the current-responsive element of the fuse. Under this ideal condition the fuse may be so selected that the transient temperature of its current-responsive element (as distinguished from the temperature of ferrules and external contacts) never exceeds a maximum critical value. Exhaustive tests have shown that this maximum permissible transient temperature is considerably lower for the overload currents of relatively long duration associated with motor operation than for the short-circuit currents of short duration normally encountered in the co-ordination of fuses in transmission-and-distribution-system applications. The maximum permissible transient temperature of the current-responsive element must be determined individually for each type of fuse.

References

1. The Current-Limiting Power Fuse, D. C Prince, E. A. Williams, Jr. AIEE Transactions, volume 58, 1939, January section, pages 11–14.

2. Control of the Switching Surge Voltages Produced by the Current-Limiting Power Fuse, E. A. Williams, Jr., C. L. Schuck. AIEE Transactions, volume 60, 1941, May section, pages 214–17.

C. L. Schuck (General Electric Company, Philadelphia, Pa.): The fuse design described by Rawlins and Fahnoe is noteworthy for its combination in the same fuse tube of three elements, each of which plays a contributing role in the interruption of fault current.

The shunt-resistor wire, the purpose of which is to limit the magnitude of transient voltage accompanying the melting of the current-limiting element, must carry the short-circuit current transferred to it until the series-connected "clean-up" element has effected interruption. During this time, heat will be developed in the shunt-resistor wire, raising its temperature. It would seem economical to utilize the full available temperature rise of the shunt resistor to its melting temperature and also its heat of fusion so as to obtain the desired heat capacity with the minimum physical size of resistor. It would be of interest to know whether the shunt resistor is so designed that it melts in the course of carrying out its intended function of voltage surge reduction.

The two current-responsive elements in the new current-limiting fuse are described as being so designed that on high short-circuit currents the current-limiting element, consisting of a relatively heavy wire with notches, melts in the notches faster than the series-connected clean-up element. The latter because of a relatively less rapid heat withdrawal by its surroundings, is depended on for initiating the interruption on currents of low magnitude at which the current-limiting element is relatively slower melting.

The notched current-limiting element calls for a somewhat different handling of the melting time–current characteristic in application problems where high transient inrush currents may occur.

A technique has been established for applying fuse units to transformer and motor circuits, in both of which inrush currents must be considered. In the paper 44–10, "Transformer Magnetizing Inrush Currents and Influence on System Operation,"[11] presented in the transmission and distribution session it was described how the I^2T or the melting ampere-squared seconds of a fuse unit could be determined quickly from the published characteristics, and how this parameter could be made use of in transformer applications to insure that the fuse unit could pass the transformer magnetizing inrush current without damage to its element. The method is quite straightforward when the lower end of the melting time–current characteristic approaches a straight line at negative slope 2:1 as would be expected from the physics involved.

Figure 4 of the Rawlins–Fahnoe paper describes the notched construction of the current-limiting element and indicates that for time shorter than 0.1 second the melting characteristic departs from that of the original diameter of the wire and in the order of 0.001 second has become completely like that of a wire having the diameter of the metal remaining in the notches. Figure 5 shows the complete melting time–current characteristics of the new line of fuse units, the lower ends of which do not break away from the 2:1 slope as would be expected from Figure 4. Assuming that the technique of Figure 4 is carried through in the design of the line of fuse, all the curves in Figure 5 should drop almost vertically as the time becomes less than 0.1 second. If this is the case, it leads to some complications in application that are not encountered when the current-limiting or the minimum section of a current-limiting fuse element has a uniform diameter. In the paper on magnetizing inrush currents previously referred to, some easily remembered figures are given to cover the integrated ampere-squared seconds of magnetizing inrush current for power transformers. The figures given are ten times full load current for 0.1 second, and the application rule recommended in applying fuse units is that the current to melt the fuse at 0.1 second should be at least 12 times normal load current, thus giving a 44 per cent safety factor in ampere-squared seconds over the transformer inrush. Although the inrush has been conveniently rounded out to figures of ten times normal and 0.1 second, it is important to note that the first few cycles of inrush are relatively far more severe and that the fuse unit must be capable of passing these relatively more severe cycles of current when they occur. If one refers again to Figures 4 and 5 of the Rawlins–Fahnoe paper, it would appear that the downward drift of the melting time–current characteristics defeats the purpose of providing adequate I^2T withstand ability in the fusible element and that additional allowance might have to be made for inrush current of transformers or the stalled rotors of motors.

According to Figure 4 of the paper, the radical reduction in section at the notches amounts to a 20-fold reduction in I^2T; therefore fuse units so designed cannot be said to have an I^2T in the usual sense. This means that, for making applications involving high transient currents for a few cycles, it will be necessary to know the melting characteristics down to 0.001 second to cover time in the order of a cycle and less. The conventional time–current plotting paper, approved by National Electrical Manufacturers' Association, extends only to 0.01 second, since for high-voltage fuses developed heretofore, the time–current curves were practically straight lines on log–log paper for times less than 0.1 second. Thus, a new method of handling the time–current characteristics of the three-element fuse will be necessary in working out application problems.

Reference

1. Transformer Magnetizing Inrush Currents and Influence on System Operation, L. F. Blume, G. Camilli, S. B. Farnham, H. A. Peterson. AIEE Transactions, volume 63, 1944, pages 366–75.

H. L. Rawlins and **H. H. Fahnoe:** The discussions presented by Mr. Williams and Mr. Schuck bring out factors particularly important in the application of the new fuse.

Mr. Williams' warning on the effect of overload currents cannot be stressed too highly. It is important to apply the fuse conservatively; and in the case of starter applications, extreme care is used to have the fuse characteristics co-ordinated with the thermal relays of the starter so that no damage can be done to the fuse. Since the enlarged sections between necks on the current-limiting fusible wire may be regarded as heat absorbers, this new fuse is adapted particularly to withstand momentary overload currents.

There is no attempt in the three-element fuse to limit the voltage with the specially shaped fusible wire. The purpose of the necked wire is to provide increased current-carrying capacity without affecting the current limitation.

Mr. Williams shows a fuse construction in which plain wires are embedded in sand. Although different diameter elements are employed, their space relationship is such that the fusing characteristic of plain wire is obtained. For the same current-limiting action, plain wire will produce four times the heat at normal rating that would be obtained with the necked wire shown in Figure 4. Therefore, a 200-ampere fuse construction as in Figure 2 is much smaller and more compact than the fuse shown by Mr. Williams and provides a definite shunt means for limiting voltage surges.

In reply to Mr. Schuck's question on the shunt-resistor wire, it can be stated that the resistor wire definitely does not melt but is good for several extra half-cycles of arcing if necessary. It is not deemed necessary to utilize the full temperature rise of the shunt resistor to its melting temperature, since the total weight of wire used in a 200-ampere 2,500-volt fuse is approximately eight ounces, and can be increased if desired.

Mr. Schuck has presented a discussion on the fusing characteristics of the new fuse based upon the assumption that the time–current curve for the complete fuse assembly will follow the curve as shown in Figure 4 of the paper. This is not the case, however, for the series fuse element always melts first and protects the current-limiting fusible element. In describing the operation of the new fuse, the paper states that, "On short-circuit currents where the series fuse element melts in a small fraction of a cycle, the current-limiting element melts almost simultaneously."

The time–current curves as shown in Figure 5 of the paper become asymptotic to the time–current curves of the element as shown in Figure 4 at time values of less than 0.001 second. The purpose of the specially shaped current-limiting fusible section is to provide a lower operating temperature for a given current-limiting action and does not result in a freak time–current characteristic. The time–current characteristic of the series fusible element is the time–current characteristic for the fuse and can be treated in the same manner as any other time–current characteristic.

High-Voltage Oil Circuit Breakers for Rapid Reclosing Duty

Discussion and authors' closure of paper 44-37 by A. W. Hill and W. M. Leeds, presented at the AIEE winter technical meeting, New York, N. Y., January 24–28, 1944, and published in AIEE TRANSACTIONS, 1944, March section, pages 113–18.

W. F. Skeats (General Electric Company, Philadelphia, Pa.): The first field requirement for high-voltage three-cycle breakers appeared about ten years ago, and low-oil-content breakers of the impulse type were placed in service on the 287-kv Boulder Dam–Los Angeles line in 1936 and have been in successful operation ever since. On the basis of this successful experience it is natural to think of extending the development to lower-voltage breakers and of increasing the breadth of its application by adapting it to dead-tank breakers.

By subtracting the apparent contact-parting time of 1.4 cycles from the interrupting-time figures given in Table II of the paper, an average arcing time of about $1^1/_4$ cycles is obtained for the range of 3,000 to 10,000 amperes. This is of the same order as obtained with another form of oil-blast interrupter as shown in Figure 8 of the Hunt, Boehne, Peterson paper presented before the Institute in January 1943[1]. With an interrupter of such characteristics, the problem of obtaining three-cycle breaker performance becomes the mechanical one of breaking the latch and separating the contacts in a very short time. Here the authors have done an excellent job.

The first 20-cycle reclosing breakers also went into operation in 1936, and one user now has over 100 of them giving satisfactory field service. Reclosing test data previously published show that the high-voltage air circuit breaker is ideally suited to 20-cycle reclosing also.

More field experience will be required with higher voltages such as 230 kv to determine what reclosing speeds can be used profitably in that range. When the demand is manifest, one may be sure that the manufacturers will be in a position to meet it.

Reference

1. Switching Overvoltage Hazard Eliminated in High-Voltage Oil Circuit Breakers, Lloyd F. Hunt, E. W. Boehne, H. A. Peterson. AIEE Transactions, volume 62, 1943, February section, pages 98–106.

C. E. Winegartner (Cleveland Electric Illuminating Company, Cleveland, Ohio): This development in high-voltage oil circuit breakers is notable for the reduction in material over that required by the old deion grid breakers, and also because it uses many of the same replacement parts, such as bushings and solenoid mechanism. In the case of the 138-kv 1,500,000-kva breaker, this reduction in total weight amounts to 30 per cent and in oil to 36 per cent. Also the tank height is reduced 19 inches and the diameter 8 inches. This seems to be made possible by the reduced travel of the bridge required with the multiflow principle.

However, from the test reports there seems to be little reduction in arcing time over the old type. Hence, with 36 per cent less oil it seems logical that the new type will require more frequent reconditioning of the oil. Since most users will probably have both the old and new types in service, a tabulation of comparable data for the two types would be of much interest.

Philip Sporn (American Gas and Electric Service Corporation, New York, N. Y.): The authors are to be congratulated on the development of a most ingenious current interrupter of the self-generating pressure type which has made possible not only advancing the limits to safe arc current interruption in oil tank-type breakers but doing so more efficiently and, therefore, with smaller equipment and at higher speed than they had been able to do heretofore. This is particularly significant in that it has come at a time when substantial development of electric-power systems has occurred as a result of the prewar and the wartime expansion programs of the various utility systems and when requirements for rupturing capacities of the order of 3,500,000 kva are not only visible on the horizon but have actually arrived.

The authors show a consistent five-cycle operation on a 138-kv breaker under factory test utilizing the simple multiorifice grid, and a 230-kv five-cycle breaker utilizing a multiorifice grid with an accelerating arm, and, again, a 230-kv three-cycle breaker utilizing, in addition to the multiorifice grid and accelerating arm, an auxiliary pump device for improving low-current operation. It is true that no field-performance test has been made to corroborate laboratory-test results. Even so, there seems to exist little reason to doubt that satisfactory performance can be obtained within the laboratory-tested limits by the breaker shown or by minor modifications of it. Granting, therefore, that the authors have the elements for controlling consistent arc action to get operation down to three cycles, one regrets that they stopped at the five-cycle operation and did not develop a single assembly adequate to give three-cycle operation for 138-, 230-, and 287-kv systems. This does not appear to be premature, considering that on the American Gas and Electric Company system nothing slower than five-cycle 138-kv breakers has been installed for a period of over five years.

This hesitancy to follow through on the elements of an excellent development is shown particularly in the case of the reclosing cycle tested by the authors. Thus, we find the authors still discussing a reclosing cycle of 35 cycles and one of 22 cycles when as long as almost eight years ago[1] this discusser showed performance on his system of a 138-kv-breaker reclosing cycle of 20 cycles and later[2] showed actual performance of similar equipment on an 18-cycle reclosure. If one considers the benefits derived on most systems by reducing reclosure time, it would appear that the proper duty to seek, if a breaker capable of three-cycle clearance is reliably attainable, is a 12-cycle reclosing duty with the time distributed approximately as follows:

Relay time	1 cycle
Breaker time	3 cycles
Deionization time and time for reclosure	8 cycles

The experience obtained to date, both on test and in actual operation, would indicate that such a cycle would be entirely practical, if the reservation that the eight-cycle deionization time would need further corroboration is always kept in mind. However, on the basis of the reduced breaker arcing time and the experience to date cited, it would seem to be an entirely safe interval.

The rupturing capacities of 3,500,000 kva should be adequate for most of the power systems in the United States over the next few years, possibly for the next five years. Considering, however, the fact that five years have a way of rolling around much more quickly than many new developments and that the electric-power systems of the country probably are destined for a further expansion, now would appear to be the proper time to start thinking about a breaker duty of 5,000,000 kva having an opening time of not over three cycles and capable of reclosing on a 12-cycle duty. It is possible that this can be done with further refinements of the present pressure-generating-chamber type of oil circuit breakers, but, if these designs should prove to have definite limitations, there is every reason to believe that either the oil-poor or the air-blast breakers can be developed to meet that specification.

REFERENCES

1. Ultrahigh-Speed Reclosing of High-Voltage Transmission Lines, Philip Sporn, D. C. Prince. AIEE Transactions, volume 56, 1937, January section, pages 81–90.

2. Experience With Ultrahigh-Speed Reclosure of High-Voltage Transmission Lines, Philip Sporn, C. A. Muller. AIEE Transactions volume 58, 1939, December section, pages 625–31.

A. W. Hill and W. M. Leeds: Mr. Skeats has referred to a form of high-voltage oil-blast interrupter described in the Hunt, Boehne, Peterson paper, presented before the Institute a year ago. Since the arcing time obtained with this five-cycle breaker is of the same order as shown for the multiflow grid in a three-cycle breaker, he infers that the only achievement is in a reduction of the mechanical time from tripping to parting of the contacts. However, he did not mention that the oil-blast device opens six pressure-generating contacts and six arc-rupturing contacts per breaker pole, whereas the highly effective multiflow interrupter utilizes but two pairs of pressure-generating and arc-rupturing contacts per pole. Fewer contacts to adjust and maintain would appear to be of considerable advantage to the user of this type of equipment.

While high-speed reclosing of high-voltage oil circuit breakers is not new, the ability to perform satisfactorily on heavy short circuits, where the effect of the first interruption on the operation of second interruption might be expected to be most pronounced, heretofore has had to be taken pretty much on faith. The multiflow grid interrupter is the only high-voltage three-cycle oil circuit breaker which has been proved by test at short-circuit currents corresponding to over 2,500,000 kva. Furthermore, this has been accomplished without resort to multibreak contacts, thus permitting the use of greatly reduced tank dimensions.

Mr. Sporn regrets that the multiflow grid development was not carried far enough to provide a single form of contact design for standard three-cycle operation at all voltages 138 kv and above. While this may be a worthy aim, it does not appear practical at the present time to produce a three-cycle breaker without some additional complication and cost as compared to a breaker for five-cycle operation. Where the higher speed is not necessary, it is desirable to use the simplest possible design consistent with adequate interrupting performance. Similar comments apply to Mr. Sporn's further suggestion of a 12-cycle reclosing time for three-cycle breakers which would require an extremely powerful mechanism that hardly could be justified economically except for very special application. For the immediate future, the use of single-pole 20-cycle reclosure appears to offer higher stability limits using substantially standard equipment.

Mr. Sporn's prediction of a still greater concentration of power, that may call for circuit breakers having a rupturing capacity of 5,000,000 kva, offers a challenge to circuit-breaker designers. With test data already obtained on a 230-kv breaker at currents corresponding to as high as 4,300,000 kva (see Figure 8 of the paper), there is no reason to doubt that the multiflow deion grid principle can be extended to meet such a rating.

Mr. Winegartner's fears that the new line of small tank breakers may require more frequent reconditioning of the oil are quite groundless. In the paper, "A Multiorifice Interrupter for High-Voltage Oil Circuit Breaker" by Ludwig and Leeds, presented before the Institute in January 1943,[1] it was stated that this new interrupter showed only between one fourth and one half of the arc energy developed in conventional breakers. This is due principally to the shorter arc length in the multiflow grid as compared to the magnetically extended arc in the older deion grids with iron plates. Also, the arcing time characteristic of the multiflow grids is fairly flat over a wide current range, the arcing time being somewhat shorter than with the older grids in the low-current range below 1,000 amperes. Furthermore, in developing breakers for five-cycle interrupting time, the contact opening speed has been increased about 20 per cent, which has lowered the arcing time accordingly. It is clear, therefore, that the drastic reduction in arc energy more than offsets the decrease in oil volume, so that the oil depreciation is actually very much less than in the older breaker designs.

REFERENCE

1. A Multiorifice Interrupter for High-Voltage Circuit Breakers, L. R. Ludwig, W. M. Leeds. AIEE Transactions, volume 62, 1943, March section, pages 119–25.

A Magnetic-Type Air Circuit Breaker for 15,000-Volt Services

Discussion and authors' closure of paper 44-47 by L. J. Linde and B. W. Wyman, presented at the AIEE winter technical meeting, New York, N. Y., January 24–28, 1944, and published in AIEE TRANSACTIONS, 1944, March section, pages 140–4.

H. V. Nye (Allis-Chalmers Manufacturing Company, Milwaukee, Wis.): This paper is an interesting description of the difficulties encountered in trying to develop the air magnetic type of circuit breaker into a reliable interrupter for 15,000-volt service. The first thing that impresses the reader after looking over the data and sketches submitted with the paper is the complexity of control and arc runner structure attained by the introduction of six separate magnetic-blowout systems into one arc chute.

One cannot help admiring the ingenious dexterity with which this is accomplished, although one may be inclined to question the engineering necessity and commercial practicability of carrying the blowout systems to such a degree of elaborateness. Since it apparently has been found necessary, even with six magnetic-blowout coils, to supplement their action with an air dashpot or booster, it seems reasonable to conjecture if an increase in the air-blast action produced by this air booster might not cut down the complexity of the magnetic-blowout structure and result in an over-all economy in material and labor. Breakers, of course, have been built, relying entirely on a self-generated air blast into a much simpler type of air chute, which operate quite satisfactorily.

The arrangement of the breaker parts seems to make it adapted to vertical-lift metal-clad switchgear, but it seems that no arrangement has been made for venting the gases from the arc chutes into the external atmosphere. I would like to ask if the authors feel that external venting is not desirable.

The data on interrupting tests show a little over two cycles to separate contacts and arcing times from 0.8 to 2.2 cycles. This appears to be a satisfactory performance if it represents what can be expected of the breaker under the worst conditions, and if there are not occasional "bad shots" when this good operation is not obtained. I notice that no data are given for currents between 600 and 1,600 amperes and would like to inquire if tests were conducted in that range. The reason for this request is that I have found on most breakers I have tested that the hardest point to get consistently good operation and short arcing times is in the neighborhood of 1,000 to 1,200 amperes on this class of breaker.

The dimensions of a metal-clad unit incorporating this breaker were not given in the paper, but would be of considerable interest for comparison with the equivalent oil breaker unit.

E. W. Boehne (General Electric Company, Philadelphia, Pa.): In the extension of the Magne-Blast breaker to 15-kv service it was necessary to develop an arc chute whose arc resistance characteristic was approximately four times that of the 7,500-volt breaker of the same kilovolt-ampere rating. This was necessary since the inductive impedance of the external circuit, which forms the criterion for the arc-resistance ohms, increases proportional to the square of the system voltage for a given kilovolt-ampere. Any increase in arc resistance in excess of this new level reduced the developed energy within the breaker and lowered the resulting recovery-voltage transient as will be clear from Figure 1 of my closure of "The Geometry of Arc Interruption—II Current-Zero Phenomena."[1]

Here is shown the relationship of arc energy and recovery voltage as controlled by the magnitude of the arc voltage. The Magne-Blast breaker functions on the "downhill side" of both the maximum initial recovery voltage (e_r) and the arc energy characteristic (W); the higher the arc voltage above 33 per cent normal voltage crest, the lower these quantities. Now, if the system voltage is raised without developing a proportionate increase in *actual* arc voltage, it follows that the value of arc voltage in per cent of the system voltage would decrease, and the trend would be to subject the breaker to higher internal energies (W) to be followed by higher recovery-voltage transients (e_r). In order to hold the internal energies low (which in turn are even smaller than are developed in some oil breakers which function on the steep left-hand side of the energy characteristic), arc voltages were developed in per cent of system voltage which took advantage of these characteristics and in addition held the peak initial recovery voltage (e_r) in the same relative position as found in the 7,500-volt design. The accomplishment of this task was achieved by the development of the 15-kv arc chute described in the paper.

The recovery-voltage crest, e_r, shown in the figure is based upon the assumption that the arc current continues to zero without diversion and, moreover, does not include resistance damping. Actually current diversion takes place in the Magne-Blast breaker which permits the current to approach zero more slowly in the final stages. This current-zero phenomenon lowers the recovery-voltage transient (as described in the paper, "Geometry of Arc Interruption—II Current Zero Phenomena"[1]) to values much less than is indicated in the accompanying figure. This control of the recovery transient is one of the unique characteristics of the interleaving arc-chute construction, and the authors are to be congratulated on its perfection as embodied in the compact design presented for 15-kv service.

REFERENCE

1. THE GEOMETRY OF ARC INTERRUPTION—II. CURRENT-ZERO PHENOMENA, **E. W. Boehne.** AIEE TRANSACTIONS, volume 63, 1944, pages 375–87; discussion, pages 447–50.

L. N. Grier (Aluminum Company of America, Pittsburgh, Pa.): J. L. Linde and B. W. Wyman have presented an excellent description of an air circuit breaker having a rating that is urgently needed by industry. It clearly sets forth the essential principles of successful arc extinguishment and indicates that considerable thought has been given to construction details that should contribute to reasonable maintenance. The provision for interrupting low current is particularly desirable. Reference is made to the prototype breaker, first rated at five kilovolts. I am happy to report that some of the earliest if not the very earliest of these breakers have been giving continuous and excellent service since their original installation in 1939. We have had more recent experience with similar breakers of 7.5-kv rating on 6.6-kv service. We will welcome an opportunity to make a 13.8-kv application, for we are strong advocates of the use of air breakers for all indoor applications. We believe that this type of apparatus in the ratings normally encountered in industrial service should or will largely or entirely replace the older oil breaker for indoor applications where hazards from ignited oil fumes exist.

This paper demonstrates the advances in the art of circuit interrupting that has taken place in the past five years, by reason of the knowledge gained concerning the nature and behavior of arcs. However, this advancement has been made, because we were not satisfied with that which went before. Let us not be entirely satisfied with the accomplishment of the electrical engineer, but let let us challenge the mechanical engineer to provide a mechanism which will permit the use of the breaker as a means of limiting the current values in the faults.

L. J. Linde and **B. W. Wyman:** We wish to thank Mr. Grier for his comments concerning the excellent service he is able to report on some of the very earliest lower-voltage Magne-Blast breakers which were the predecessors of the breaker described for 15-kv service in this paper. The similarity of design and interrupting performance which this breaker exhibits should win for itself an equally enviable service record.

As pointed out by Mr. Boehne in his discussion, the arc resistance in ohms for a given kilovolt-ampere interrupter necessary to affect the same current reduction increases as the square of the system voltage. This may be brought about either by greatly increasing the size of the arc chute or by increasing its efficiency. The latter has been chosen as the most compact and economical design. Mr. Nye apparently does not agree with this, as he questions the use of six blowout coils. It has been found that a strong uniform magnetic field is one of the most important requisites in obtaining maximum efficiency per unit volume of arc chute. It is clear that a single strong blowout coil cannot even approach a uniform magnetic field over an arc chute of reasonable dimensions, as the field strength decreases as the square of the distance from the coil.

The current-reduction characteristics of this arc chute force a reduction in the values of this current as it travels out along the arc runners. Blowout coils placed out on the runners have a larger number of turns than those at the arc-chute throat. This increased number of turns compensates for the reduction in current to hold the magnetic field approximately constant as the arc travels through the chute to interruption. When the breaker is closed, these coils make up a high-impedance circuit in parallel with the intermediate and primary contacts. Therefore, they are required to carry current only during interruption. They have no moving parts and need very little insulation. Their entire assembly is not complex and offers no maintenance problem. It is fortunate that the required strength of the magnetic field is a function of current, for this magnetic-blowout structure automatically adjusts its field strength to the values of current to be interrupted. We have found that self-generating air-blast breakers generally lack this characteristic. A description of such a breaker might be of considerable value to the Institute.

It never has been found necessary to vent the metal-clad enclosure of the Magne-Blast breaker, as the arc-chute muffler so effectively cools the exhaust gases that no fire or luminous gases are visible even on interruption at full breaker ratings. It is difficult to imagine what kind of magnetic breaker Mr. Nye had in mind when he questioned the interrupting performance between 600 and 1,600 amperes. As this happens to be in the rated carrying-current range, life tests exceeding 10,000 operations have been run at these values of current as fast as two per minute without replacement of any parts. As the arc lengths in this range of current never exceed three cycles, the arc runner and contact erosion was very mild.

The basic arrangement of this breaker provides for space economy in its final application. A complete metal-clad equipment, including current transformers, cable pot heads and bus, has an over-all width of 36 inches, a height of 82 inches, and a depth of 81 inches. Of these dimensions the height and depth are approximately ten per cent more than those of the equivalent oil breaker. The width remains the same.

Aircraft Signal Systems

Discussion and author's closure of paper 44-5 by Raymond A. Rugge, presented at the AIEE winter technical meeting, New York, N. Y., January 24–28, 1944, and published in AIEE TRANSACTIONS, 1944, January section, pages 3–5.

J. P. Dallas (nonmember; Los Angeles, Calif.): In reference to "Aircraft Signal Systems" by Raymond A. Rugge, there are many aircraft electrical engineers who disagree with the primary assumptions as well as the final conclusions of this paper. A brief statement of the basis of this disagreement may be in order.

There are three major defects in airplane-instrument-panel warning-light signal systems:

1. The most important objection is that the lamps burn out. The life of aircraft warning lamps is unpredictable, because of the conditions of vibration and shock incident to aircraft operation. Since the safety of an airplane may depend on the reliable operation of its warning signal devices, it seems unfortunate that this point was neglected almost entirely in Mr. Rugge's consideration of the problem. It is common practice to use two lamps in the more important warning lights and to provide special switches and/or relay circuits for testing. Such methods are not entirely effective and add considerable weight and complexity to the warning signal system. Mr. Rugge states that a minimum signal-lamp life of 250 hours is satisfactory, while rejecting visual electromechanical warning signals with a life exceeding that of the airplane.

Perhaps the case for the visual electromechanical warning signal should be reexamined. It requires no additional external lighting. The same lighting which is safe enough and effective enough for the instruments may be designed to be adequate for electromechanical visual warning signals. If such signals are designed and installed properly, it is believed that they will adequately attract the pilot's attention.

2. The second defect in aircraft-warning signal lights is that they are both hazardous and ineffective unless their brightness is adequately regulated to suit a wide range of flight station light conditions ranging from bright sunlight to almost complete darkness necessary in night flying. Mr. Rugge accurately describes the complications of this phase of the problem and proposes a photoelectric dimming control in addition to the dimming resistors, relays, and switches now employed. It is to be noted that the visual electromechanical type of warning signal eliminates all of this complication, since it may use the lighting already provided for the instruments.

3. The third defect common to many aircraft warning-signal systems is that the signals do not warn adequately or are confusing because of arrangement. This confusion is not properly a fault of the particular signal device used, but rather a product of instrument panel design or signal application. We all agree that a warning signal should be instantly clear. Mr. Rugge proposes to accomplish this end by the use of illuminated signs contained in a single "telltale" signal box. To many it is questionable whether this type of signal is as quickly effective as the signal functionally placed on the instrument panel. A warning signal placed in the immediate proximity to the indicating and control equipment is thought by some to be more effective. Thus, it is questionable whether oil-temperature warning signals mixed with landing gear and other unrelated warnings in a common "telltale" box are as effective as an oil-temperature warning signal immediately under the oil-temperature indicating instrument, attracting the pilot's attention to this particular airplane function.

Mr. Rugge has proposed two devices, each a solution to one of the phases of this problem, namely: a photoelectric dimming control and an illuminated sign type of telltale signal device. While these two devices may be meritorious, it is unfortunate that the three general design principles stated in this paper seem to have been developed from a consideration of these two devices rather than from a survey of the whole problem. Thus, the whole question of lamp failures, spare lamps, replacements, and the relative weight and space requirements of the various devices has been given little consideration. It is proposed, therefore, that the basic design requirements of aircraft-warning signal systems may be more completely stated as follows:

1. *Signal Effectiveness.* A warning signal must adequately warn. This covers Mr. Rugge's first two design principles: "Signals must be absolute," and "signals must be distinct."

2. *Signal Reliability and Safety.* Under this heading must be considered the probability of failure of all warning signal devices including lamp burnout, replacement, spares, and testing means, as well as the operational hazards of dimming.

3. *Signal Weight and Space Requirements.* Weight is always a major aircraft design consideration, and space limitations are critical on the aircraft instrument panel.

F. Von Voigtlander (Schwartze Electric Company, Adrian, Mich.): The author has presented a very commendable paper on visual aircraft signal systems. The three design principles set up can be applied effectively to the design of all types of signaling systems, whether for aircraft, industrial, institutional, or municipal use.

Visual signals have an important place in many signaling applications, but years of experience in this field have shown that really to satisfy the first two design principles, namely that the signal must be absolute, and that the signal must be distinct, the visual signal must be supplemented with an audible signal. This is believed to be especially so in the application of signaling equipment to military combat aircraft. The problem of signaling in combat aircraft is complicated considerably by the great physical stress and nervous tension under which the crew operates in the combat theater, and by the high degree of probability that circumstances will develop requiring the positive functioning of the warning signals and prompt response to them by the crew. Furthermore, the intense strain of the concentration of the crew on the job being done would be largely on the eyes. This could mitigate greatly the distinctness of the visual signal and render it unnoticed for a dangerous length of time. Though the automatic dimmer might go a long way toward eliminating the hazard of the glare of the lamps to night sight, it might not add much to their distinctness as signals in the critical moment when they might be most needed.

The suggested telltale device (Figure 3 of the paper) equipped with the automatic dimmer and supplemented with proper audible signals should make a very effective means for aircraft signaling. The audible signals would sound whenever any telltale was lighted. An acknowledgement switch would be provided to cut off the audible signal manually and at the same time to prepare it automatically to sound again upon repetiton of the signal or upon the receipt of another signal. In addition to this, some form of lamp supervision would be included to mitigate the need for multiple lamps for each of the visual indicators.

This type of centralized signaling combining audible with visual has been used for years in countless industrial, commercial, and public-utility applications with consistent success. The addition of the audible signal reinforces the effectiveness of the system by providing a second signal, thereby greatly extending mechanical and electrical reliability; by appealing to a separate sense, thereby greatly reducing the probability of the signal not being observed; and by providing for manual means of acknowledgement.

A really adequate signaling system should go far to relieve the crew of much fatigue in attempting to keep track of a veritable maze of instruments during flight. Such a system would be supervised properly automatically and would contain sufficient elements, so that in the absence of any indications to the contrary the crew could assume that all conditions were normal without any reference to instrumentation.

An example of the manner in which a system of this sort could function follows. Assume that fuel level becomes dangerously low. The fuel-level switch would light a signal lamp on the annunciator panel. At the same time the audible signal would sound its warning blast. The pilot would operate manually the cutoff switch silencing the audible signal. He would then watch his fuel gauge to observe the behavior of the fuel level and to verify the alarm indication. On verification he would cut in his reserve fuel supply and would restore the annunciator to normal by operating a "reset" button. The indicating lamp would then go out, but the related circuits all would be set up to receive a repetition of the alarm should the fuel level fall again. In the meantime, the system could respond to any other indications for which facilities were provided, the audible signal sounding for each case.

To date the use of audible signals on aircraft has been confused by the lack of standardization of these signals for various applications. The organization with which the writer is identified is currently manufacturing seven different types of audible aircraft signals. There is now under way a program for the standardization of these signals together with the development of much lighter and more effective devices, and it is expected that all audible signaling requirements for aircraft can be met with two or three standard lightweight types.

The author has considered aircraft signal systems chiefly from the standpoint of the pilot. Of considerable importance, especially on larger craft, is the problem of signaling the other crew members for jump signals, low oxygen, and so forth. In military aircraft audible signals have been found very useful for such crew signals. The types now being developed for proposed standardization will be superior to the older types used for this purpose and should permit the installation of much more effective audible signaling systems.

W. A. Petrasek (American Airlines, Inc., New York, N. Y.): There are few subjects in aircraft engineering as controversial as that of aircraft signal systems. Mr. Rugge is to be complimented on his initiative in bringing up the subject and welcoming discussion. There is a definite need for an exchange of ideas on this subject as evidenced by the many varied and haphazard signal systems which have been installed in aircraft to date.

I should like to present my personal slant on this problem, drawing on my experience and association with one of the most exacting types of pilot in the business—the transport pilot.

In order to determine intelligently the best type of signal system to do a job, it not only is necessary to think along engineering lines, but also, and more important, the knowledge and experience of the psychologist must be considered. The problem is basically one for the psychologist, namely, what is the best stimulus and in what manner should it be applied so that the pilot will respond in the manner intended?

One of the axioms which I would recommend as a basis for guidance in signal-system applications is that any installation designed to attract the pilot's attention will also distract it. There is an obvious corollary to this axiom, that is, the greater the number of warning units (or stimuli) to which the pilot is exposed, the greater is the possible confusion in his reaction to them and the greater is the possibility of hazardous distraction. It follows, therefore, that the ideal signal system will employ a minimum of warning units consistent with attaining the objective of safe flight.

In considering the problem of warning-system distraction, I believe that the pilot should be given some means of removing the signal completely when, in his judgment, it interferes with his safely piloting the airplane. For examples we might cite the condition of an aircraft approaching a landing field at night under adverse conditions of visibility. A bright or even dim light in the pilot's field of vision could be the reason for an accident. A blaring horn easily could interfere with the pilot's attempt to give verbal instructions to the copilot or other crew members in an emergency so as to delay the pilot's corrective action to the point of disaster.

Obviously, any application of a cutout device must be so designed that under no circumstances could it be left inadvertently "on." It is here that the ingenuity of the engineer is summoned. The natural reaction to this suggestion is to view it with some disfavor and to consider it as a loophole in an otherwise foolproof signal system when the pilot is provided with a means of disconnecting the warning at his option.

There is only one way to prevent such an abuse of a signal system, and that is to design and install a system which will be reliable functionally. Repetitious false signals will result in the pilot's utter disregard of the warning system, and you can rely on his finding some ingenious way of eliminating the distraction. This action will vary from pulling the fuse in the circuit to placing a blob of chewing gum over the light. Who can say that the pilot is at fault, for the entire safety of the flight is placed in his hands when he boards the airplane.

Raymond A. Rugge: This is in reply to the discussion furnished by J. P. Dallas regarding "Aircraft Signal Systems." Mr. Dallas discusses the defects of instrument-panel warning-light systems and proposes that the design requirements of signal indicators should be signal effectiveness, signal reliability and safety, and weight and space requirements, rather than the objective principles described in the paper that signals should be positive, distinct, and not a hazard.

The controversy raised by Mr. Dallas in his discussion is apparently predicated on the premise that the selection of aircraft signal indicators should be attained by placing equal emphasis on long life, weight, space, testing, replacement, spares, testing means, operational hazards, and signal effectiveness. Although all of these items are important and none must be overlooked in considering signal indicators, it was the purpose of the article to stress the objective design principles relating to the selection of aircraft signals rather than the detail factors. As was stated in the synopsis of the paper, the pilot's requirements or primary objectives for signal indicators have been overlooked in too many cases with the result that the selection of the signal indicators has been based on the minor rather than on the major objectives.

The primary objective to be accomplished by the design principles mentioned in the paper is to provide the pilot with warning signals and informative signals that cannot be ignored, and, in addition, such signals must not create a hazard or disturbing distraction. The secondary objective in selecting the signal indicators is to provide signal indicators that satisfy requirements for long life, ease of replacement, and minimum weight and space. To satisfy the primary objective the author of this paper believes that only light or audible signals can be used. Target-type signals or electromechanical indicators obviously satisfy most of the secondary objections but do not satisfy the primary objectives, for the reason that they are in most cases overlooked or ignored because of the small contrast with surrounding items.

An example of the importance of selecting indicators based on the primary objectives is brought out in the latest proposals again to use indirect-lighted instruments. In this case it would be necessary to provide indirect lighting to activate or illuminate the targets of the electromechanical indicators.

Mr. Dallas mentions that the objections to instrument-panel signal lights are due to lamp burnouts, unpredictable life of the lamps, and the fact that lamp filaments are often destroyed by vibration and surge voltages. Yet, lamps with these same vulnerable features are used to energize or to illuminate the targets of electromechanical indicators.

The author agrees with the comment made by Mr. Dallas that it is common practice to use lamps in parallel to provide a more reliable signal indicator. This item should have been mentioned in the paper, as the telltale device incorporates this idea of using two lamps in parallel for each signal indication. The fact that it is common practice to install signal lights in parallel in airplane signal installations and the fact that the purpose of the paper was to stress the primary objective principles to be used in selecting signal lights accounts for the absence of such secondary requirements in the paper.

Potential Breakdown of Small Gaps Under Simulated High-Altitude Conditions

Discussion and author's closure of paper 44-29 by M. J. DeLerno, presented at the AIEE winter technical meeting, New York, N. Y., January 24–28, 1944, and published in AIEE TRANSACTIONS, 1944, March section, pages 109–12.

R. C. Mason (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The breakdown voltage under uniform field conditions, and for values considerably larger than the minimum increases only slightly less rapidly than does the product of gap length and gas density. Near the minimum of the familiar sparking-potential curve, the change of voltage with pd is slight. For nonuniform fields, however, the breakdown voltage for a given electrode separation does not increase nearly so rapidly as gas density does; and further, the variation depends upon the field distribution and hence will not be the same for all separations of a given electrode system.

Thus, one sees why in Table III of the paper the ratio of air-gap breakdown voltage at sea level to that at 45,000 feet changes from 2.7 to 3.5 with different arrangements. Furthermore, for the change in density represented by these two altitudes, of about 7, in a uniform field, the breakdown-voltage ratio could have been very close to 1 for separation near the minimum; while for larger gaps a voltage ratio of 5.5 could have been found. Since such a large range in values is possible, it hardly would seem advisable to specify a test ratio of 2.7 to 3.5 unless one is very sure that all apparatus under all conditions would duplicate the half-dozen test arrangements of Mr. DeLerno.

The principle of similitude shows in general the impossibility of taking any definite ratio for breakdown voltages at two pressures for practical apparatus of fixed dimensions, since it is only for *geometrically similar* systems that the functional relation quoted by Mr. DeLerno holds.

L. G. Levoy, Jr. (General Electric Company, Schenectady, N. Y.): This paper presents some very interesting information which will be useful to the designers of aircraft electric equipment and also will assist the various standardizing groups in setting up standards for high-potential testing of devices such as relays, contactors, terminal boards, and so forth where the insulation to be tested is of the nature of air gaps or surface creepage.

Where solid insulation is involved, such as the slot insulation in a motor or generator, the altitude factors given in the paper do not apply, and care should be taken not to overstress such insulation by conducting sea-level tests based on equivalent high-altitude high-potential test voltages established by air-gap or surface-creepage breakdown phenomena.

M. J. DeLerno: The fact that the altitude ratios given in Table III of the paper do not apply in cases where solid dielectrics are involved has been discussed thoroughly by Mr. Levoy. In this paper no attempt was made to study the effect of altitude on the puncture strength of solid dielectrics. It is known that an increase in frequency causes an increase in dielectric-heating loss and a corresponding decrease in dielectric strength. Altitude affects corona-starting voltage which in turn may affect the ultimate puncture strength of the insulation. Variations in humidity and ambient temperature also affect the dielectric constant of solid dielectrics. These factors are sufficient to warrant an experimental investigation to determine the effect of altitude on solid dielectrics.

The point mentioned in Mr. Mason's discussion concerning the inadvisability of using breakdown ratios at sea level to 45,000 feet of from 2.7 to 3.7 unless the tests conducted on the various specimens represent all possible conditions which could exist on aircraft equipment is certainly true. No exception can be taken with the statement that the principle of similitude applies only for geometrically similar systems. Whether the tests conducted on the various specimens will represent all possible conditions which could exist on aircraft equipment will now be discussed. It is first necessary to realize that these ratios were established only for the purpose of specifying minimum high-potential levels for testing proposed 208-volt 400-cycle equipment.

For electrode spacings between one-eighth and one-half inch, the ratio at sea level to 45,000 feet for a uniform field varies from 5.0 to 5.1. As Mr. Mason points out, for nonuniform fields the breakdown voltage for a given gap does not increase nearly so rapidly with density as it does for uniform fields. Therefore it would seem that a ratio as high as this never would be reached for the nonuniform field configurations present in aircraft equipment for gaps between one-eighth and one-half inch.

Gaps of less than one-eighth inch never should exist between conductors having a potential difference of 208 volts, because such gaps would have potential-breakdown values too close to the minimum allowable breakdown values. For a one-eighth-inch electrode spacing at 45,000 feet, the value of pd (gas pressure in millimeters of mercury times electrodes spacing in millimeters) is 346, whereas the value of pd which gives the minimum breakdown potential for uniform fields is 4.5. These values seem far enough apart to say that for a one-eighth inch gap the ratio of breakdown potential at sea level to 45,000 feet should not approach the unity value mentioned by Mr. Mason.

If the electrode spacing exceeds one-half inch, where ratios of 5.5 may exist, the possibility of breakdown at voltages lower than two kilovolts seems quite remote. Glaring defects such as carbonized paths, metallic short circuits, will appear if a voltage of 3.7/5.5 of actual rated high voltage is applied. It will be seldom that spacings between live parts on 208-volt aircraft equipment will exceed one-half inch. Under these conditions it seems justifiable to neglect the inaccuracy of applying too low a high-voltage level on spacings exceeding one-half inch which may be brought about by ratios exceeding the 3.7 value mentioned in the paper.

Corona in Aircraft Electric Systems as a Function of Altitude

Discussion of paper 44-59 by W. R. Wilson presented at the AIEE winter technical meeting, New York, N. Y., January 24–28, 1944, and published in AIEE TRANSACTIONS, 1944, April section, pages 189–94.

R. C. Mason (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): In this paper references are quoted to show that sparking (or corona) cannot occur at less than 351 or 275 volts, depending on which authority one believes, yet, experimentally Mr. Wilson found what he calls "corona" to start in one case at 80 volts rms (113 peak). Is there a real contradiction here? I believe not.

For the establishment of a self-maintained discharge of a certain type, both experiment and theory agree that a definite minimum voltage is required. The class referred to is a discharge maintained by ionization by collision—a term covering a variety of effects, some quite familiar and others not yet even completely understood. The magnitude of the minimum voltage depends on the kind of gas present, the electrode material, and the nature of the surface, and only to a small extent on the uniformity of the field. For air with most common metals as electrodes, the minimum voltage is in the neighborhood of 300 volts, or say between 250 and 350. Much lower voltages can occur, however, for the rare gases with alkali electrodes. The value of 113 volts appears to be much too low for air.

Discharges can be started, however, at lower voltages than the minimum mentioned previously, if phenomena other than those covered by ionization by collision come into play. As examples, a heavy-current a-c arc may reignite, even with nonthermionic electrodes, in a circuit with voltage less than the minimum; or an arc can be started by an ignitor with quite low voltage applied. Then too, current can flow from a cold negative electrode by field emission, the criterion being the magnitude of the electric field at the surface rather than the total voltage. Since, in the experiment cited, the surface resistance was quite low, it is very plausible that ionization in the gas space was not the only factor involved. Indeed, since the only observation reported by Mr. Wilson was made on an oscilloscope screen, it is not at all certain that a true gas discharge existed.

True corona, using the word in the commonly accepted sense of a discharge involving only the complex known as ionization by collision, would not occur with the common metals in air at 113 volts; it is perfectly possible, though, at 294 volts (208 rms).

Electric-Circuit Burning-Clear and Damage Phenomena on Aircraft Structures

Discussion and authors' closure of paper 44-58 by C. M. Foust and J. G. Hutton, presented at the AIEE winter technical meeting, New York, N. Y., January 24–28, 1944, and published in AIEE TRANSACTIONS, 1944, April section, pages 198–204.

W. M. Davidson (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): In testing aircraft cables to determine their ability to burn clear when subjected to faults there are many variables. The problem in aircraft circuits is even more complex than in industrial distribution systems because of the scarcity of data on the nature of faults. In the laboratory logical assumptions must be made to determine comparative effects of different system voltages, available fault current, and the nature of the fault itself.

Mr. Foust and Mr. Hutton have approached this problem by setting up a power source which is a replica of a heavy bomber system and connecting this source to cable faults with closely controlled contact pressure and restricted cable movement. In Figure 2 of the paper the authors show their cable arrangement for a phase-to-ground fault. The cable appears to be bound over most of its length to the rigid supporting arm of a small test stand. The writer assumes that the varying amounts of fault-contact pressure were obtained by adjusting the position of this arm. Such an arrangement places the emphasis on contact pressure and, according to the paper, makes current level the only other important factor. This is a fundamental approach to the problem, but it does not seem a good approximation of actual conditions that might be encountered in aircraft faults. In the case of faults resulting from gunfire there is little likelihood that cables may be held against the plane structure with more pressure than can be supported by the natural flexure strength and weight of free cable. In order to interpret Mr. Hutton's tests for faults of this nature it would be necessary to determine the contact pressure corresponding to each cable size and compare it with the pressures used in these tests. It seems logical and more practical to assume a given free length of damaged cable arranged in its normal position parallel to the structure with the faulted end resting lightly against the metal surface. By using the same cable arrangement at all values of current and voltage, the effect of other variables such as cable size and air pressure can be compared more readily. Also, with the latter arrangement the cable is free to bounce and restrike as it would under actual conditions.

An extensive series of d-c and 400-cycle short-circuit tests has been made by the writer using power sources of adjustable fault-current capacity and cable faults which approximate actual service conditions. In these tests the effect of cable size on contact pressure and restriction of motion was produced directly.

If aircraft-cable fault tests were to be completely rigorous, it would be necessary to include a wide range of fault-contact pressure, area, and thermal capacity. At one extreme are faults caused by frayed conductors where only a few strands are in light contact with a metal structure. At the other extreme are faults where the conductor is pinned against a large metal object. In the former case thermal capacity in the fault is less than in the cable, while in the latter case thermal capacity in the fault is greater than in the cable. The two types may be termed, respectively, "low-thermal-capacity" and "high-thermal-capacity" faults.

From a large number of tests it has been possible to determine approximate current levels above which light-contact faults clear rapidly with out welds or damage to insulation and also the levels below which insulation damage and welds or sustained arcing will usually occur. Between these two extremes damage may or may not occur, depending on the thermal capacity of the fault. In order to represent the degree of fraying in large cables, tests on smaller cables of the same cross-sectional area as the active portion of the larger cable may be taken as an approximate criterion. Beyond these extremes even high-thermal-capacity faults gave similar results, although the time of clearing was affected. In all cases of serious damage and fire, welding occurred at initiation of the fault. No welds or damage to insulation were observed for currents above approximately 2,000 amperes with number 8 American Wire Gauge, 1,000 amperes with number 14, and 300 amperes with number 20 wire. The conducting time associated with these currents is in the range between approximately 0.1 second for number 20 and 0.5 second for number 8. For the lower levels a maximum clearing time of 30 seconds was selected, because the damage involved in a fault of this duration definitely would be intolerable, and the corresponding currents would be below the minimum safe limit. These minimum currents are independent of system voltage. The average values from a large number of faults are as follows: 450 amperes with number 8, 175 amperes with number 14, and 60 amperes with number 20.

R. H. Kaufmann (General Electric Company, Schenectady, N. Y.): The experimental work which forms the basis of this interesting paper, presented by Mr. Hutton, along with the results of similar investigations performed by others, is of immense value in building up a good conception of aircraft electric fault behavior.

The reported results give increased impetus to the adoption of a thorough high-speed automatic protective system. The factors which I believe to be most significant, particularly with respect to low voltage (24–30 volt) d-c systems, are:

1. While many faults may burn clear, an indefinite period of arcing (up to several seconds) may be encountered, during which time the expulsion of molten metal constitutes a distinct fire hazard, and the persistent flow of high current detrimentally affects the operation of the entire electric system. Practical fault-protective equipment can interrupt automatically the fault-current flow within 0.01 second, a condition which would limit the energy liberated at the point of fault to one per cent of that associated with an arcing fault of one-second duration.

2. Even faults which do clear themselves continue to constitute a hazard. The frayed ends of electric-power conductors which have not been de-energized can produce repeat faults as a result of vibration or movement of personnel within the ship, possibly to reach and attend an injured companion. Photographs of extensive exterior damage on returning ships as widely distributed seem to indicate that many internal areas would assume an appearance somewhat as illustrated in Figure 9 of this paper.

3. Some high-current faults may not burn clear and thus demand automatic fault interruption to allow continued operation of any portion of the electric system.

C. M. Foust and **J. G. Hutton:** The authors wish to thank R. H. Kaufmann and W. M. Davidson for their respective constructive discussions on the subject paper.

As Mr. Kaufmann pointed out, many photographs of airplanes damaged in actual combat operations have been obtained. These indicate that damage of the type shown in Figure 9 of the paper occurs in practice, and consequently it is expected that the type of short circuit and damage investigated covers a major portion of the range of wiring short circuit and damage occurring under combat conditions. It is hoped that the tests will be of value in determining the proper design or selection of protective equipment.

With reference to Mr. Davidson's discussion, the cable arrangement shown in Figure a of the paper was not used in the tests with 2 circuit of the type given schematically in Figure 1. As was pointed out in the text, the d-c tests were performed on a portion of a "Flying Fortress" and with a replica of its electric circuit, no artificial restraint being placed upon the short-circuiting cable. The cable mounting shown in Figure 9 of the paper, though applicable only to the gunfire tests, is indicative of this.

The movement of the conductor was not restricted, except in those cases where information was desired on the variation of arc length with applied voltage. In Figure 7 is shown a series of photographs extracted from high-speed moving pictures of the progress of a d-c short circuit. The initial position of the conductor is shown at *a*, and its movement under the influence of magnetic forces can be observed by comparing this position of the conductor with that indicated in Figure 7f, where the short circuit has just cleared. The majority of tests were performed with the movement of the conductor unrestricted.

The 28.5-volt d-c tests were completed before similar work was commenced on the 120/208-volt a-c system. With such an increase in voltage, once a short circuit was completed, a substantially longer arc was expected, and the conductor was set up according to Figure 2 to verify this. As soon as it was established that the results of the d-c and a-c tests were very similar, and that in the range of voltages considered the pressure and current level were the important factors in determining whether or not a short circuit would clear, the method used in applying short circuits in the d-c tests was adopted for the a-c tests. The conductor was free to move under the influence of magnetic forces and to restrike as long as conductor or aircraft structure remained to establish a short circuit.

Through a range of pressure and contact area the type of short circuit may vary, from one involving very light contact between the structure and only one or two fine strands of conductor, to one involving many strands compacted under pressure against the airplane structure. The effects of these two extreme types of short circuit are readily predictable, and hence only few tests were made with them. The effects of the intermediate short circuit are the most troublesome to predict. In these the contact area, pressure, and current are such that sputtering occurs at the short circuit which may or may not burn clear. Consequently the majority of short circuits were applied in the intermediate range.

Effect of Altitude on Electric Breakdown and Flashover of Aircraft Insulation

Discussion and author's closure of paper 44-57 by L. J. Berberich, G. L. Moses, A. M. Stiles, and C. G. Veinott, presented at the AIEE winter technical meeting, New York, N. Y., January 24–28, 1934, and published in AIEE TRANSACTIONS, 1944, pages 345–54.

R. C. Mason (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Paschen's law, as the authors have explained and emphasized, applies only for uniform fields. As the engineer often must deal with nonuniform fields, the more general Principle of Similitude, of which Paschen's law is merely a special case, really should be the guide in gas-discharge work. As applied to gas breakdown, the principle of similitude requires all linear dimensions of the apparatus to be multiplied by the same factor which divides the gas density, in order for the starting voltage of the discharge to be unchanged; it is not enough merely to change the separation of the electrode. Further, with nonuniform fields, breakdown voltage is not a function of *pd* alone.

Consequently, not only for the practical reason cited by the authors, but because of violation of the principle of similitude, results of overpotential tests at sea level cannot be carried over directly to high altitudes in general, for apparatus which necessarily is surrounded by nonuniform fields. To be certain of results, one should measure breakdown voltage at air density corresponding to the desired altitude.

Because, for the rod gaps, breakdown voltage is not a function of *pd* alone, and also $V \neq f_1(p) \cdot f_2(d)$, the principle of similitude explains the reason different curves exist for different separations in Figure 12 (and probably also in Figure 18) of the paper. Just a casual inspection of Figure 7 of the paper is enough to show one that for the rod gaps used in this paper the breakdown voltage is not a function of equivalent *pd*.

For uniform fields, the d-c breakdown voltage above the minimum increases only slightly less rapidly than gas density, for constant electrode separation. It is because of the nonuniformity of field that the breakdown voltage reported in this paper increases much less rapidly than gas density does.

W. M. Green (United States Army Air Forces, Dayton Ohio), **C. A. Maple** (United States Army, Dayton, Ohio), and **J. B. Crockford** (Westinghouse Electric and Manufacturing Company, Lima, Ohio): Altitude-chamber-test results always are questioned until tests under actual conditions show that the chamber results are correct. Flight-test results obtained by the flight test unit of the electrical branch of the equipment laboratory, engineering division, Wright Field, Dayton, Ohio, have shown that high-altitude brush wear can be studied in a properly constructed and operated altitude chamber. This organization also conducted flight tests to check the data presented in the paper.

Equipment capable of producing from zero to 15-kv direct current was installed in an airplane suitable for high-altitude flight, and tests were conducted at altitudes of approximately 10,000, 20,000, and 30,000 feet. At each altitude tests were run on rod gaps of $^1/_{32}$, $^1/_{16}$, $^1/_8$, $^1/_4$, and $^1/_2$ inch and the flashover across $^1/_4$ inch of material *A* of the paper (paper-base phenolic-bonded Micarta covered with two coats of iron-oxide-filled alkyd-resin paint). Five readings were taken for each of the five gap settings and for the flashover material. All altitude readings obtained are corrected to the National Advisory Committee for Aeronautics density altitudes.

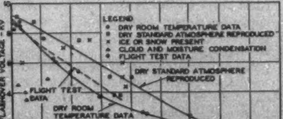


Figure 1. D-c flashover voltage versus altitude for material *A* with one-fourth-inch electrode spacing under various atmospheric conditions

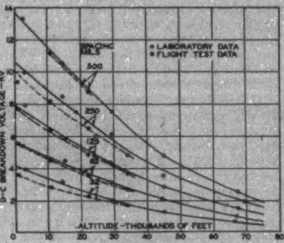


Figure 2. D-c breakdown voltage versus altitude

Figure 1 of the discussion shows the comparison of the flight-test data with the chamber results presented in Figure 20 of the paper. At 10,000, 20,000, and 30,000 feet the temperature of the air through which the airplane was passing was minus 12, minus 26, and minus 53 degrees centigrade, respectively, which indicates that the moisture content of the air was low. The breakdown and flashover tests were conducted at temperatures higher than the standard temperatures and less than room temperature. Consequently, the flight-test data should lie between the dry-standard-atmosphere and dry-room-temperature curves. The actual curve obtained substantiates this conclusion.

In Figure 2 of the discussion, the flight-test d-c breakdown voltages for the five rod-gap spacings are compared with those presented in Figure 10 of the paper. Within the expected accuracy of ten per cent, the curves check well. It is noted that in every case the flight-test points are below those of the paper.

From the curves obtained in flight it is concluded that chambers can afford invaluable means of predicting d-c flashover and breakdown voltages for various altitude conditions.

Further flight tests will be conducted in a similar manner to determine the accuracy of the a-c breakdown and flashover chamber data presented in the paper.

Victor Siegfried (Worcester Polytechnic Institute, Worcester, Mass.): It is interesting to see that the problem of insulation has been carried to high altitudes, with especial interest in the matter of desirable spacings in air for high voltages. From the Paschen's law relationship, the expectation of reduced strength of air is certain. It is well, however, that these actual tests have been made, for it is shown that the breakdown strength of the air insulation is in the same direction but is not in direct proportion to the air pressure.

In evaluating the effect of altitude, the one common factor that would apply to all gaseous insulation is relative air density. If the effects described could be related to air density, it will give us a tangible factor covering both pressure and temperature as altitude varies. In practically all the data presented, the strengths of air do not decrease as rapidly as air density. This is in line with some work done on corona loss at high altitudes,[1,2] where the factors are much the same, but it was necessary to suppress considerations of altitude, as such, and to consider air density. The effect of this factor was analyzed and it was found that corona starting voltage was proportional to the two-thirds power of the air density. While this exponent may not apply accurately at high altitudes, it would seem to represent the general effect of the data. I suggest this method of representing the data as a function of air density so as to crystallize the results into a simple representation of the altitude factor. Possibly some other exponent will be preferred, especially as the effects will differ for different configurations. It is certain from data taken up to extremely high altitudes, however, that this exponent will be less than unity. In case of doubt, the linear relationship will be the conservative one, and actual test data will permit reductions of spacings beyond that.

REFERENCES

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2. ELECTRICAL ENGINEERS' HANDBOOK (book), Pender, Del Mar. Volume IV, section 14, pages 34–5.

L. J. Berberich: The authors in particular want to thank Captain Green, Lieutenant Maple, and Mr. Crockford for their flight-test results which materially add to the value of our paper. The close agreement obtained between flight- and chamber-test results indicates that cosmic rays do not affect the results appreciably, as was to be expected. Since in the flight test, ultraviolet light was prevented from irradiating the gap by the metal fusilage of the plane, the flight-test results give no information regarding the effect of ultraviolet light. We therefore made some tests at atmospheric pressure in which the gap was irradiated with a mercury-vapor lamp. These results showed that ultraviolet light produced no measurable effect on either the breakdown or flashover voltage. Both cosmic rays and ultraviolet light, however, are known to affect the impulse breakdown because these radiations reduce the time lag.

We are indebted to Doctor Mason for extending our discussion of gaseous breakdown phenomena. The principle of similitude can be invoked to explain qualitatively why the breakdown voltage decreases less rapidly than the relative air density in nonuniform fields, but it is extremely difficult, if not impossible, to apply quantitatively. Therefore, we agree with Doctor Mason that any given electrode configuration should be investigated experimentally.

As to Professor Siegfried's remarks, we can predict from the principle of similitude that for nonuniform fields the breakdown voltage is related to air density through some power less than one. However, the exact relationship between breakdown voltage and air density is very definitely influenced by electrode configuration. Therefore, we believe that it is fortuitous that our data indicate the two-thirds power relationship found in other work.

A Multiplied-Deflection A-C Potentiometer

Discussion of paper 44-21 by R. B. Marshall, presented at the AIEE winter technical meeting, New York, N. Y., January 24–28, 1944, and published in AIEE TRANSACTIONS, 1944, February section, pages 77–80.

P. A. Borden (The Bristol Company, Waterbury, Conn.): In reading Professor Marshall's paper I could not but be reminded of the somewhat similar work described in a paper which I presented before the Institute several years ago.[1] At that time, however, many of the modern units which Professor Marshall has been able to utilize in making his apparatus portable were not available. As a result, the apparatus which I described could not in any sense be considered as portable. It was possible, though, with this equipment to obtain extremely satisfactory results in the measurement of a-c quantities where the amount of power available for purposes of measurement was negligible in magnitude.

It might be well at this time to call attention to the asynchronous method of detecting a balance condition, which proved very satisfactory in a number of the measurements there described. This involves either a dynamometer type of movement having its field excited from a source slightly out of synchronism with the network under measurement, or a permanent-magnet moving-coil galvanometer including in its circuit a commutator driven at a speed slightly above or below synchronism. The use of the asynchronous commutator in a measuring circuit has been described by Chubb and Fortescue[2] and by Bedell,[3] but neither of these makes reference to the utilization of the device in the detection of balance, in which application the advantage of the method is especially apparent.

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1. BALANCE METHODS IN A-C MEASUREMENT, **Perry A. Borden.** AIEE TRANSACTIONS, volume 42, 1923, pages 395–402; discussion, pages 402–05.

2. CALIBRATION OF THE SPHERE-GAP VOLTMETER, **L. W. Chubb, C. Fortescue.** AIEE TRANSACTIONS, volume 32, 1913, page 739.

3. USES OF THE SYNCHRONOUS COMMUTATOR IN A-C MEASUREMENTS, **F. Bedell.** *Journal* Franklin Institute, October 1913, page 385.

Crossed-Coil Power-Factor Meters

Discussion and author's closure of paper 44-33 by N. P. Millar, presented at the AIEE winter technical meeting, New York, N. Y., January 24–28, 1944, and published in AIEE TRANSACTIONS, 1944, June section, pages 294–301.

P. A. Borden (The Bristol Company, Waterbury, Conn.): To one who has read the literature on power-factor measurement as published during the past 30 years, it would seem almost impossible that any further contribution of value could be produced. Mr. Millar, however, appears to have accomplished the task and made a valuable addition to the theoretical discussion of the subject, and particularly to the laws underlying scale conformation.

It is rather unfortunate that the author, after touching upon the subject of power factor in unbalanced circuits, has summarily dismissed it as presenting a problem impossible of a practical solution. While it may be that the treatments of this subject which from time to time have appeared in the technical press agree in showing that the true mathematical concept (if any) of this magnitude does not lend itself to ready expression, measurement, or utilization, yet a practical embodiment which has been available since the subject first became one of general interest is still at hand. The measurement of reactive power or vars, as accomplished with indicating, recording, or integrating instruments having an artificially introduced 90-degree phase shift, has proved satisfactory in the work of the public utilities and regularly is made the basis of billing for electrical-power loads. This would seem to be the prime requirement; and, if a measurement thus based on vector power factor is sufficient as a means of establishing charges for the use of electric energy, it certainly should be accurate enough for the purposes of an indicating switchboard or portable instrument.

The author has made no reference to that form of power-factor meter in which both the potential and the current circuit are provided with polyphase windings. While it is not known that instruments of this type have been produced commercially in this country, for many years they have been common in European practice. A theoretical analysis of their operating principles shows that the power-factor readings which they provide are in agreement with the accepted definition of vector power factor, and that they remain so without respect to balance or unbalance conditions of the load. A conclusive test of this property of the instrument is found in the fact that, if a polyphase load be made up of a number of components having different magnitudes and different power factors, and the power factor of this load as a whole be determined by means of an instrument of the type under discussion, the indication will remain unchanged, whatever distribution of the several load components may be made among the different phases of the system.

The following list of references, deal with the practical aspect of determination of power factor on an unbalanced load. It is interesting to note that *vector power factor* (sometimes called "Italian power factor") while frequently credited to Campos, was previously suggested by an American authority, Burt, in the AIEE TRANSACTIONS.

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2. THREE-PHASE POWER FACTOR, **Austin Burt.** AIEE TRANSACTIONS, volume 27, 1908, pages 801–17.

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6. ELECTRICAL MEASURING INSTRUMENTS (book), **Drysdale, Jolley.** D. Van Nostrand Company, Inc., New York, N. Y., 1924. Volume 2, page 252.

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W. N. Goodwin, Jr. (Weston Electrical Instrument Corporation, Newark, N. J.): Mr. Millar's paper on crossed-coil ratio meters was of especial interest to me, as I had to make the same analysis and arrived at the same results about 35 years ago when Weston power-factor meters were first developed. The graphs used by Mr. Millar, however, are ingenious and should prove helpful in designing scale modifications.

Mr. Millar gave consideration to the use of the power factors described by him on unbalanced polyphase systems, stating that the indications of such instruments were meaningless for unbalanced systems.

In a discussion which followed Mr. Millar's paper, Perry Borden also referred to power factors for unbalanced polyphase systems and said that, as far as he knew, no American manufacturer had made such an instrument, at least commercially. In 1914 one of my associates, B. P. Romain, and I jointly developed a polyphase power-factor meter for unbalanced loads, at the Weston Corporation laboratory. It was based, however, on balanced voltages, and, as is the case with other power-factor meters, it was also based on sinusoidal current and voltage, but was otherwise correct for any degree of current unbalance.

The instrument actually measures the ratio of the reactive volt-amperes to the power in watts, which corresponds to the tangent of the power-factor angle. That is, for example, in a three-phase three-wire circuit:

Tan of phase angle

$$=\frac{E_1I_1\sin\phi_1+E_2I_2\sin\phi_2+E_3I_3\sin\phi_3}{E_1I_1\cos\phi_1+E_2I_2\cos\phi_2+E_3I_3\cos\phi_3}$$

The instrument was similar to the usual polyphase wattmeter in that it had two field coils and two sets of movable coils mounted on a common staff. Instead, however, of having one movable coil for each field coil, two crossed coils were used for each field coil. Two of the movable coils, one in each field coil, which lie in the same plane are connected to the circuit in the conventional manner as a wattmeter, so that the torque exerted by them is proportional to the actual power in the circuit. The other two coils, spaced at right angles to the wattmeter coils are so connected to the circuit that their currents are in quadrature with the currents in the corresponding wattmeter coils, and their torque is proportional to reactive volt-amperes. The equilibrium position therefore, is at the point where the two torques balance, at which position the ratio of the reactive volt-amperes and power is indicated. This ratio now is known as vector power factor. When these instruments were developed, however, the only recognized definition of power factor was the ratio of the power in watts to the volt-amperes, and, since the instrument did not indicate in accordance with this definition, it was decided not to offer the instrument commercially. I believe this decision was somewhat unfortunate, as vector power factor since has been recognized and defined, and also all other forms of power-factor meters indicate vector power factor.

Paul MacGahan (Westinghouse Electric and Manufacturing Company, Newark, N. J.): This paper is concerned principally with explaining an analytical approach to the design problem of obtaining required scale characteristics in the phase-angle meters which we somewhat erroneously call "power-factor meters." All makes of present-day moving-coil power-factor meters are derived from the electrodynamic single-phase crossed-coil type originally described by Tuma in 1897. They measure the angle of lag between a current and a set of voltage vectors, and the scales are marked in cosines of this angle. Therefore, strictly speaking, they are not power-factor meters at all but can be said to read power factor under the ideal condition of sine wave and balanced polyphase circuits. They can be calibrated readily by means of applying variable percentages of the normal rated a-c voltage to the two moving-coil systems. A Variac, or a slide-wire potentiometer and voltmeter can be used as a comparison standard, as indicated in the diagram and table of Figure 1 of this discussion. The basis for this method is closely in agreement with Mr. Millar's analysis leading to his data for scale development. Mr. Millar's instrument, Figure 1a of the paper, differs from the Tuma type in having a separate current coil for separate moving coils, the latter being adjustable as to angle. This angular adjustment is apparently for the purpose of securing different scale-distribution arrangements. The same effect is obtained by electrical means in other crossed-coil instruments by inserting resistance in series with the common connection of the moving coils, or by shunting one moving coil by a resistance.

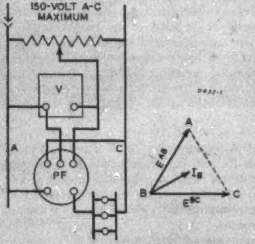

Figure 1. Three-phase 100-degree-scale power-factor meter calibrated by variable-voltage method

Potentiometer Rheostat of 1,000 Watts May Be Used

Power-Factor Scale	Voltage Settings—Per Cent of Normal Volts	
	Coil *AB*	Coil *BC*
100	100.0	100
90 lag	58.5	100
80 lag	39.6	100
60 lag	13.1	100
50 lag	0	100
30 lag	−28.9	100
20 lag	−47.3	100
90 lead	100.0	58.5
80 lead	100.0	39.6

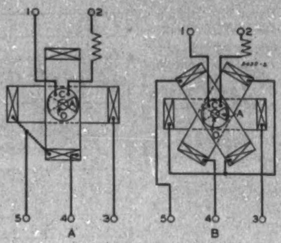

Figure 2

A. Internal connections of two-phase types *F* and *I* power-factor meters

B. Internal connections of three-phase types *F* and *I* power-factor meters

The first American power-factor meters were designed by Conrad and myself (patent 695,913) in the year 1899 (Figure 2 of this discussion) and comprised distributed two- or three-phase current-coil windings surrounding a moving iron vane polarized by a single-phase voltage coil. This was intended by the inventors as a power-factor meter suitable for moderately unbalanced polyphase systems. But the values read under unbalanced conditions were questionable and not well understood. This was long before we had our modern definitions of power factor, var, and volt-ampere on unbalanced polyphase circuits, and before the days of symmetrical components, a method of analysis which might have been applied to the action of that early polyphase power-factor meter. The design was changed later to have polyphase voltage coils and a single-phase current coil, and it frankly then became a cosine θ meter the action and calibration of which are clearly understandable. This was the predecessor of modern circular-scale power-factor meters.

For operating purposes, the simple three-coil cosine θ type of power-factor meter is well accepted, although vectoral types of unbalanced load power-factor meters are available in recording types, and as an added feature in the familiar recording kilovolt-ampere-demand meters.

N. P. Millar: In the discussion of this paper most of the comments made by P. A. Borden, W. N. Goodwin, and Paul MacGahan are of historical interest and point out the ways power factor can be measured on unbalanced polyphase systems.

There is one impression that I wish to correct concerning the measurement of polyphase power factor. In the second paragraph of Mr. Borden's discussion it is intimated that, after touching upon the subject of power factor in unbalanced circuits, I had dismissed it as representing a problem impossible of a practical solution. It is recognized that special instruments are available for unbalanced power-factor measurements, but any detailed analysis or treatment of this measurement problem is not within the scope of this paper. Since the paper made clear the necessity of the balanced condition for polyphase applications of the crossed-coil-type power-factor indicator, it was felt that it might be of value simply to point out the limitations of this instrument for unbalanced conditions.

Influence of Improved Magnetic Alloys on Design Trends of Electrical Instruments

Discussion and authors' closure of paper 44-31 by M. S. Wilson and J. M. Whittenton, presented at the AIEE winter technical meeting, New York, N. Y., January 24–28, 1944, and published in AIEE TRANSACTIONS, 1944, March section, pages 100–04.

Paul MacGahan (Westinghouse Electric and Manufacturing Company, Newark, N. J.): The authors have presented one more important example of a fact that is being recognized gradually by instrument engineers—that principles of operation having been well established, and that modern methods of manufacture with highly developed tool equipment having been more recently applied, the designers now look to new materials for the next big step in progress. Among new materials, the magnetic alloys are the most important which have become available for instrument use.

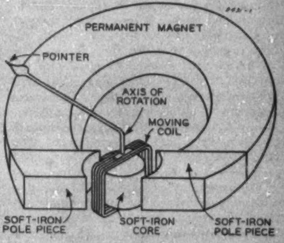

Figure 1. D'Arsonval-type concentric double air gap

The first step in this direction was the development of the portable oscillograph[1] by J. W. Legg, based upon the use of the cobalt magnet steel as developed by P. H. Brace.[2] This revolutionized the entire art of oscillography.

Since that time, the cobalt steels and later the aluminum alloy groups have made possible many new applications of commercial instruments to measurements not previously possible. Examples such as the thermocouple ammeters for radio frequency, the rectifier types for audio frequencies and d-c microammeters, and low-reading millivoltmeters, are now common practice.

The further advantages of high coercive types of magnets are, of course, their greater permanency under adverse conditions of the strong demagnetizing fields often found in practice.

As inferred by the authors, the disadvantage heretofore has been the element of cost which restricted the use of the new magnets to applications for which the normal sensitivities obtainable with tungsten or chromium magnets were not sufficient. This greater cost is due to two things:

1. The greater costliness of the material content.
2. The much greater cost of machining, drilling, and of magnetic treatment.

Judging by Table I of the paper, the authors' new magnet steel can best be compared to the standard 36 per cent cobalt steel, over which it has roughly a 20 per cent advantage in H or a 15 per cent advantage in $B \times H$. However, the authors' reference to the use of a modification of Comol for the specific instrument described seems insufficiently definite.

I would like to inquire whether the authors recognize any cost advantage, in the case of ordinary or normal sensitivity forms, such as generally used for conventional d-c voltmeters or ammeters, over the scheme of using soft-iron pole pieces attached to chromium–steel magnets, as shown, for example, in Figure 1 of this discussion.

In the case of high-sensitivity instruments which necessitate high-coercive magnets, would not the cost advantage of the authors' construction depend upon whether the "age-hardening" process successfully maintains the exact dimensions of the machined magnets so as to avoid further expensive machining? The authors do not give any information on this matter nor on the age-hardening method itself. It would be of interest to many who may not be familiar with this term to explain it further.

The writer is inclined to agree with the authors' remarks concerning laminated punched-magnet construction. Here, any apparent reduction in direct labor and material cost might be far offset by the expense of tool upkeep, and the necessity of each lamination being handled and processed separately.

The writer may, perhaps, add to the value of the discussion by mentioning another pertinent observation which is that these new high-magnetic alloys are causing a comeback in the use of magnetic damping, instead of air damping for instruments. Many of the new circular-scale instruments would not have been practicable without the use of some of these magnetic developments.

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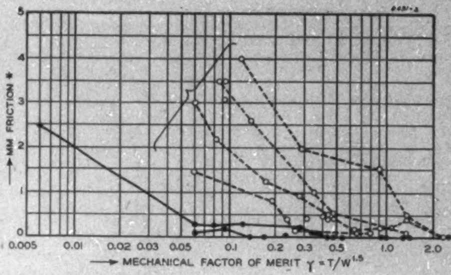

Figure 2. Friction versus factor of merit for horizontal- and vertical-shaft position

* To express friction in millimeters is not correct. It should be expressed as a force or a momentum. However, it is usual to express deflection changes due to friction in millimeters

George Keinath (consulting engineer, Larchmont, N. Y.): In the authors' paper the mechanical factor of merit is given differently for vertical and horizontal shaft, the weight with the 1.5th power for vertical shaft, and the straight weight in grams for horizontal shaft. This would mean that for weights above one gram there is less friction with horizontal shaft.

It seems to me that the factor of merit should be calculated without consideration of the position of the shaft; however, with a vertical shaft much smaller factors of merit can be allowed than with a horizontal shaft.

About 15 years ago I tried to find a relation between the allowable factors of merit for horizontal and vertical bearings by testing about 30 instruments from eight manufacturers for friction in horizontal and vertical position. Although the results were, of course, very erratic, they can be put into an empirical formula

$$\gamma_{\text{hor}} = \gamma_{\text{vert}} + \sqrt[3]{\gamma_{\text{vert}}}$$

This means that we have the same friction for an excellent instrument in horizontal-shaft position with $\gamma = 2.0$ as we have with a vertical shaft with γ only $= 1.0$. A vertical shaft with $\gamma = 0.01$ is as good or as bad as a horizontal shaft with $\gamma = 0.23$—that is, 23 times higher.

Figure 2 of this discussion shows the friction versus factor of merit for horizontal- and vertical-shaft position. Although the points cannot be connected with a curve, it can be seen that for a horizontal shaft even for a good instrument the factor of merit should not be below 0.3 or 0.5, while for vertical shaft it can go down as far as 0.1 or even 0.05.

The mechanical factor of merit however should not be overemphasized. It is only good for the manufacturer to compare the quality of different design possibilities. The user of instruments may sometimes find that an ammeter or voltmeter with a factor of merit as low as 0.2 is better than another design with a factor of merit of 0.4 and higher. In other words: the absolute value of the factor of merit means very little when applied to different designs, and even for similar designs its value should not be given with exaggerated accuracy, never more than two decimals. The quality of materials used for the pivots and jewels which cannot be included in the formula are most important.

L. W. Matsch (Commonwealth Edison Company, Chicago, Ill.): This paper presents in a manner easy to follow a review of the considerations affecting the design of d-c instruments. I would like to see more papers written in this manner presented before the Institute.

However, I have been unable to reconcile the relative sizes of the Comol and chrome magnets shown in Figure 5 with the data listed in Table II. In that table the volume of the chrome magnet is 11.5 cubic centimeters against a volume of 10.6 cubic centimeters for the Comol magnet. The ratio of air-gap fluxes, Comol to chrome is 1,980/1,495 or 1.325. For equal amounts of air-gap flux the volume of the chrome magnet would be only $(11.5/10.6) \times 1.325 = 1.574$ times the volume of the Comol magnet. In Figure 5 of the paper this ratio of volumes seems much greater. The authors state that the conventional armature with outside pivots can be used with the Comol magnet. From Figure 10 of the paper it would seem that the converse is equally true, that is, that the inside pivot armature can be used with the chrome magnet. If this is true, then there are two independent factors influencing the size of the new instrument:

1. An improved magnet.
2. An improved pivot arrangement.

Was the pivot arrangement the same for both instruments of Table II of the paper, or did the instrument with the chrome magnet have the pivots on the outside of the armature? If the latter is the case, it would seem that the superiority of the new instrument over the conventional chrome-magnet instrument as indicated by the data in Table II is not entirely due to improvement in the magnetic circuit.

M. S. Wilson and **J. M. Whittenton:** As Doctor Keinath has mentioned, the mechanical factor of merit should be used only to compare similar instruments and is one factor in determining the relative quality of different designs.

As pointed out by Mr. Goss,[1] a more accurate comparison results if the factor of merit is taken as $T/W^{1.5}$ in comparing instruments with a vertical shaft and T/W for instruments with a horizontal shaft. From a practical point of view these formulas show that under average conditions an increase in the weight of the moving system will result in a greater relative increase in the bearing friction of an instrument having a vertical shaft than in a similar instrument having a horizontal shaft.

An accurate comparison of the effect of friction between a vertical- and horizontal-shaft instrument cannot be made, however, without taking into account all factors which cause bearing friction under each condition.

For this reason the factor of merit for a vertical-shaft instrument and that for a horizontal-shaft instrument cannot be compared directly. For the usual instrument, as pointed out by Doctor Keinath, satisfactory operation can be obtained with a lower factor of merit in the case of a vertical-shaft instrument than in the case of the horizontal-shaft type since the friction radius of the pivot is inherently less in the former.

With reference to Mr. Matsch's discussion regarding the relative sizes of the Comol and chrome magnets as shown in Figure 5 of the paper, in comparison with the magnets from which the data in Table II were taken, it should be noted that the sizes as shown in Figure 5 are for the ideal magnet.

While the dimensions of the magnet referred to in Table II are made as close to the ideal as practical, they are controlled to a large extent by the available space, with the result that the maximum amount of magnetic energy may not be obtained for the volume of material used.

As mentioned by Mr. Matsch, it is correct that the size of the new instrument as well as the characteristics, was influenced both by the magnet design and the fact that an armature having internal pivots was used. The improved characteristics, however, were due primarily to the Comol material and the design of the magnet.

The instrument having the chrome magnet, the characteristics of which were given in Table II, was of the conventional construction utilizing an armature with pivots mounted on the outside of the coil.

Mr. MacGahan has brought out some interesting points relative to the use of high-coercive-force materials in instruments.

With regard to the advantage of using the Comol material in instruments having normal sensitivities, the principal gain is in uniformity of design for instruments covering a large range of sensitivities. This standardization obviously has advantages from the manufacturer's as well as the user's standpoint.

With reference to the effect of "age hardening" on the dimensions of the magnet gap, it has been found in practice that this can be controlled sufficiently to obtain satisfactory results although minor changes do occur.

Mr. MacGahan suggests that further information on the age-hardening process might be desirable.

Briefly, age hardening or "precipitation hardening" as referred to in this paper might be described as a hardening of the alloy brought about by time or a low-temperature treatment by which a component or components, previously in solid solution in the alloy, are caused to precipitate out in such a manner as to cause physical and magnetic hardening. Since this is a very complex subject and cannot be covered in sufficient detail here to be of value, the reader is referred to references 2 and 3 of this discussion, if further information is desired.

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An Eight-Year Investigation of Lightning Currents and Preventive Lightning Protection on a Transmission System

Discussion and authors' closure of paper 44-50 by E. Hansson and S. K. Waldorf, presented at the AIEE winter technical meeting, New York, N. Y., January 24–28, 1944, and published in AIEE TRANSACTIONS, 1944, May section, pages 251–8.

G. D. McCann (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The data reported in this paper are of great value in furthering our knowledge of lightning phenomena, particularly because of the thoroughness with which the investigation has been conducted. The installation of surge-crest-ammeter links on every tower of the lines studied has enabled the determination of the density of strokes to the lines and the variation of this with time and location. This is one of the most fundamental factors in the lightning performance of transmission lines for which little information has been available from any other source.

The small number of discharges recorded at station lightning arresters indicates the effectiveness of well-protected lines in preventing surges at stations. However, in the lightning investigation being conducted co-operatively by a number of utilities and the Westinghouse Company, it has been found that quite frequently magnetic links supposedly capable of recording currents as low as 200 or 300 amperes will not show the presence of surge currents as high as 800 amperes. This is thought to be due principally to demagnetization by the proportionately high arrester power follow currents as compared with the low surge currents.

The lightning arresters being studied have been equipped with various recording devices, some of them capable of measuring currents as low as 0.1 ampere. Most of the study is being conducted on lower-voltage circuits than those reported in this paper. However, during a one-year period at two substations connected to 110- and 132-kv shielded lines, eight arrester discharges were recorded, none of which gave an indication on the magnetic links designed to record down to 200 amperes. It has been found that, for station arresters on 22- to 33-kv circuits connected to exposed unprotected lines, about 50 per cent of the arrester discharges are below 800 amperes, and about 60 per cent of these would not have been detected by the surge-crest-ammeter links.

An analytical study that has been made of the effect of indirect strokes on transmission lines[1] provides direct data on this point. Figure 33 of reference 1 of this discussion indicates that most of such discharges on a well-shielded line are due to indirect strokes. For lines of the 110- to 132-kv class an average of one to three discharges per year is indicated at the end of a single line. It is true that most of these will not impose serious duty on station equipment; however, experience over long periods of time, with either unprotected or inadequately protected transformers connected to well-shielded lines, has shown that a certain percentage of these can cause winding failures.

Table X of the paper presents data on the maximum value of surge impedance of an overhead ground wire. These data are based on measured values of crest-tower currents and calculations of the voltages that strokes to a ground wire can produce on the ground device and the phase conductors. Two of the most important factors in such calculations are the wave front of the stroke current and the voltage induced on the phase wire. It is not thought that sufficient data were available on these factors to provide a reasonable estimate of surge impedance. For instance, with the span lengths considered, a one-microsecond wave front must be assumed if the midspan ground-wire voltage is to be anywhere near the product of the surge impedance and crest-surge current. It has been found in the Westinghouse investigation that about 70 per cent of all strokes have fronts exceeding two microseconds. If a value of two microseconds had been used, the surge-impedance figures of Table X would have been doubled, since the midspan voltages then could be only one-half the values actually calculated. The authors also point out the absence of midspan flashover, indicating it to be due to the low surge impedance. It is thought, however, that the principal reason for good line performance in this respect is because midspan clearances usually have been calculated on the basis of a one-microsecond front, whereas, most strokes have longer fronts. Such calculations, therefore, have been quite conservative.

Reference

1. Induced Voltages on Transmission Lines, C. F. Wagner, G. D. McCann. AIEE Transactions, volume 61, 1942, pages 916–30.

E. R. Whitehead (Duquesne Light Company, Pittsburgh, Pa.): Hansson and Waldorf deserve the gratitude of all transmission engineers for their persistence in collecting, analyzing, and presenting such valuable data under present conditions.

The data of Table III of the paper are of especial interest to those concerned with estimating the performance of transmission lines, and a cursory examination of those for line *A* suggests that the application of certain principles of probability theory might furnish a fair estimate of the limiting stroke frequency. Whenever new or modernized lines designed for improved lightning protection are placed into service, there is a great deal of interest in their performance for a few years, while the design details are fresh in one's mind, and it is frequently difficult to give an adequate explanation of the probable variations in performance.

As an illustration of this problem, I should like to cite the performance of two groups of transmission lines on the Duquesne Light Company's 66-kv system. A total of 24.4 miles of double-circuit steel-tower construction was rebuilt with six insulators on wood arms, a greatly elevated shielding wire with an angle of about 27 degrees, and an underline ground wire to provide high coupling. In three years of service there were two tripouts, one of which was known to be caused by flashover on the rebuilt line, and one which could have been on a tapped line having poor shielding. Since the predicted flashover rate was 0.71 per year compared with the observed rate 0.67, it might seem as if the method of calculation were quite accurate.

Now a second line 28.4 miles long was rebuilt with exactly the same shielding and coupling wires and number of insulator units, but without the wood arms. In two years of operation this line has experienced eight tripouts, or a rate of four per year, compared with the predicted rate of 2.3 per year. Here it might appear that the method of prediction was very poor indeed. In this latter case there are known to be a number of towers with air clearances below design standard which cannot be corrected under present conditions, and one flashover was located at one of these points.

When the data furnished by the authors, coupled with statistical principles, are applied to the performance data given in this discussion, the difference becomes understood more easily.

R. L. McCoy (Locke Insulator Corporation, Baltimore, Md.): Hansson and Waldorf again have made a significant contribution to the accumulation of knowledge and information regarding the design and operation of high-voltage transmission systems. They are to be congratulated on the scope and thoroughness of this long-range investigation on which they have reported periodically through various channels of technical expression.

Of particular interest is the random nature of the lightning-stroke variation on the same line from year to year, as well as the information obtained regarding individual tower histories over relatively long periods of time. As the authors so ably have pointed out, this shows the danger of drawing conclusions hastily from information accumulated on this subject over a period of two or three years.

I should like to ask the authors three questions bearing generally on the subject of this paper, their opinions on which would be valuable in making recommendations for the construction of future lines.

1. It is inferred that the benefit accruing from a counterpoise system can be measured almost entirely, as it contributes to reduction in footing resistance down to the point where the footing resistance is equal to or less than $^1/_{100}$ of impulse flashover of the insulators. When a line is to be constructed in deep sandy soil, where even an extensive counterpoise system would not yield a low footing resistance, would they recommend that the counterpoise system be dispensed with in favor of deep grounds constructed at each structure to attain low footing resistance? It is conceivable that, under these circumstances, obtaining low resistance in this manner might not result in low impedance from the ground wire to ground because of the distance to the true ground plane. Since it is recognized that this is a difficult problem to which there generally has been no definite answer, do the authors' experience and data indicate any trends which would help on this problem?

2. In making recommendations for the construction of lines, one is always confronted with the economic balance between the cost of a flashover and the attendant outage and the cost of preventing it. I note that on the 220-kv system the authors believe the protection provided is about 99.9 per cent efficient, based on 11 years operation. It is pointed out that improved protection would be necessary to attain perfection. If a system is considered where continuity of service is important as it is on theirs, and based on the operating experience obtained, do the authors think that further improved protection can be obtained *economically?*

3. If the lines are taken as a whole or split up as to voltage and type of construction, it would be interesting, if the authors would care to disclose it, to know the average cost per mile of the counterpoise and ground-wire system.

The information contained in the appendix of the paper, showing sample calculations for estimating the performance of any type of construction as to lightning outages, is interesting. The results in the cases calculated show good agreement between calculated and realized performance, when the nature of the problem is considered. These calculations are based, of course, on known occurrences in a given area over a period of several years. In other areas where such information is not available it would be impossible to make a calculation on exactly this basis. Do the authors think that weather-bureau data as to the frequency and severity of lightning could be used and interpreted into their experience to give a very rough estimate of the projected performance of a line, if the range of footing resistances were assumed and the counterpoise ground-wire–insulation combination were known or assumed?

M. S. Oldacre (Commonwealth Edison Company, Chicago, Ill.): As a result of the extensive research over the past 15 years on the subject of lightning phenomena, the protection of overhead lines against lightning has approached an engineering basis. Thus, it is possible today to design transmission lines, the degree of immunity from lightning troubles of which can be estimated with a fair degree of accuracy. It becomes, consequently, of the greatest interest to the design engineer to follow the performance of such lines to determine how well their operating records fulfill his engineering predictions. The authors of this paper have made a comprehensive study along these lines, and their findings indicate that these relatively new design methods are surprisingly accurate. Although, of course, this is not the first published study of this type, it seems unique in rounding out the story of an engineering project begun several years ago by the authors of this paper.

The question of midspan flashovers has never been settled adequately. Theoretically, it has been considered possible for strokes to midspan to cause flashovers, and a number of lines have been built with differential sagging between ground wires and phase conductors to provide high insulation levels at midspan. The authors present data to show that fairly severe strokes can contact the ground wires of extra-high-voltage lines at midspan without causing flashovers. I am inclined to object, however, to their paragraph 19 under "Summary," regarding a change in ideas as to the surge impedance of overhead wires, which may be misleading. The low surge impedances obtained from the data in Table X of the paper by no means would hold if all pertinent factors, including coupling between phase and ground wires, incomplete cancellation of voltage by the returning ground-wire wave, and the surge impedance of the lightning stroke itself, are taken into account. If we include these factors and use calculated surge impedances of the wires, the conclusion is reached that no flashovers should have occurred, as was the case, for any of these records. As a matter of fact, the absence of flashovers at midspan, as well as at the towers, seems easily explained by the relatively conservative design of the line. Presumably, this was either line *A* or *F* which, according to Table XII of the paper, had a perfect lightning record. It is agreed, nevertheless, that large midspan spacings may be justified as a means of preventing sleet troubles.

The operating record of our 220,000-volt Powerton–Crawford line, placed in service in 1940, may be of interest. In spite of the fact that sleet formation and conductor galloping have been observed on this line, they have caused no outages. At one section of the line, ice that had fallen off the wires had been found that had a maximum thickness at one point of about seven-eighths inch. As far as lightning performance is concerned, there have been no outages definitely attributable to lightning. In April 1942 the line tripped at a time when lightning was observed near the north end. However, records of fault current indicated a ground fault 80 or 90 miles from the north end, so that some doubt exists as to the actual cause of tripout. No burns could be found on the wires when a survey was made at the indicated location of the fault.

R. H. Golde (The British Electrical and Allied Industries Research Association, Greenford, Middlesex, England): By continuing their statistical investigations and presenting them in such a lucid manner, the authors have done a great service to all those concerned with or interested in the protection of high-voltage transmission lines against lightning hazards. As the authors show so well, the frequency of lightning strokes to individual parts of a transmission system varies so greatly in different years that only long-term observations are capable of providing a clear picture of the susceptibility to lightning incidents and of the variation of their severity. All the factors covered in the paper warrant comments but considerations of space limit the following remarks to such points only as either appear to require further elucidation or give rise to additional queries.

The vexed question as to whether there do or do not exist particular stretches of a transmission line which are exceptionally prone to lightning is tackled by the authors in the most suitable manner, that is, by examining the number of times individual towers actually were struck. Table IV of the paper thus leads them to the conclusion that the distribution of lightning strokes occurs at random. If it is assumed now that this statement is mathematically correct, Table I of this discussion is obtained in which the numbers of towers which should have been struck once or more, twice or more, and so forth, are calculated according to the law of probability and compared with the authors' counts which have been rearranged accordingly.

There exist various ways to explain the deviations which appear in Table I, particularly on line *B*. The fact that the observed results show, in general, lower values than expected in the cases of more frequent strokes to individual towers, however, may suggest that some such repeated strokes did actually occur but remained unnoticed, owing to the inability of magnetic links to record the occurrence of repeated current discharges.

The frequency distributions of tower currents due to lightning, which are shown in Figures 2 to 4 of the paper, suggest a curve of the double-hump type with the second hump at about 30 kiloamperes. The great number of observations on which these curves are based leads the authors to the suggestion that "there may be two types of lightning discharges." Now, comprehensive investigations[1] have shown such a consistency in the characteristics of lightning discharges all over the world that a departure from the accepted picture would be most important. Before accepting this explanation, however, it may be useful to mention that the distribution curves shown comprise in fact tower currents due to two different causes, that is, currents produced by direct strokes to towers and the higher tower currents discharged in consequence of strokes to an earth wire somewhere along the span. An independent investigation of the distribution of lightning currents shows that, for equal current amplitudes in the lightning channel, these two types of tower current may be in the ratio of up to 2 to 1, this ratio decreasing with increasing footing resistances. It follows that the distribution curves of tower currents due to the two types of strokes mentioned are bound to differ, and it may well be possible that they are the reason for the double-hump curves found by the authors. In any case, a further detailed examination of the individual cases would appear necessary before the suggestion of two different types of lightning discharges could be accepted.

The unequal subdivision of the currents discharged through individual tower legs, which is clearly presented in Table VII of the paper is rather startling, particularly in the high-current range. Usually the unequal current distribution is explained by variations in the earthing resistances of the individual legs, but, under investigation, this explanation should not apply to most of the towers of the lines which are provided widely with symmetrical systems of counterpoise wires. It would be most interesting to learn from the authors whether the high currents given in Table VII are observed in towers without counterpoise, and whether a further subdivision of the ratios of leg currents into towers without and with symmetrical counterpoise might confirm the aforementioned suggestion.

The authors' experience as to simultaneous disturbances on two parallel lines, the towers of which are bonded by means of a counterpoise, are most interesting. Actually, it appears that such bonding may even have harmful effects in that any stroke to one line impresses the same potential on both systems and thus enables simultaneous back-flashovers to occur. In the absence of bonding, a certain potential drop would occur in the earth between the towers, and this certainly would reduce the likelihood of a back-flashover on the unaffected line. In soil of high resistivity it might even be most advantageous to stagger the tower positions of parallel lines so as to reduce the coupling through earth to a minimum.

Table I

Number of Times Struck Exceeding	Towers Struck the Indicated Number of Times or More			
	Line A		Line B	
	Calculated	Observed	Calculated	Observed
0	477	477	179	179
1	366.56	362	155.84	156
2	204.76	199	108.34	93
3	86.49	93	59.77	52
4	29.20	22	26.76	26
5	8.22	7	9.98	7
6	2.12	2	3.17	3
7	0.64	0	0.87	1
8	0.33	0	0.21	0

The value of the surge impedance of an overhead earth wire appears to be too well established to permit of any deviations from theory. While occurrence of corona at the potentials quoted in Table X of the paper may reduce the surge impedance to values of about 350 ohms, a further reduction appears unlikely. The solution of the mystery appears to be in the authors' tacit assumption of lightning currents reaching their peak values after about one microsecond. All data available[1] suggest a front duration of about five to six microseconds for the average lightning current, and, though steeper current rises certainly do occur, they just do not reach the values of over 70 kiloamperes per microsecond required by the authors' calculation, or, if they ever do, their frequency of occurrence is very small. It thus follows that the flashover voltages assumed in Table X should be divided by only a fraction of the total currents indicated, and this will result in maximum values for the surge impedance well above the value of about 350 ohms. However, it should be added that the authors are quite correct in their suggestion that customary distances between earth and phase wires are ample, with respect to the risk of flashovers at midspan.

The authors' method of calculating the probability of back-flashovers is very useful and bound to give results of the correct order. In the detailed calculation of the number of flashovers on a particular line, there appear to exist, however, two slight errors which, by coincidence, roughly cancel each other, thus giving the correct answer. As mentioned previously, the frequency distribution curve of tower currents given in Figure 2 represents the sum of two different distributions which are based on strokes to towers and earth wires, respectively. Therefore, it is not strictly correct to apply this curve to the calculation of the probability of back-flashovers due to strokes to towers or earth wires, as given in the appendix of the paper. Secondly, in the case of the line without earth wire, it is assumed that 20 per cent of the strokes hit the towers and 80 per cent the phase wires. This assumption is based on model tests which have been shown liable to give misleading results.[2] Actually, from some analytical work it appears that the ratio of strokes to towers and earth wires suggested by these model tests is quite erroneous.

I should not wish to end these remarks without expressing the hope that the authors will continue their most valuable observations, and that they will again publish them as soon as sufficient data have been accumulated.

REFERENCES

1. SURGE PHENOMENA. ERA, London, England, 1941. Chapter 1.

2. R. H. Golde. *Journal*, Institution of Electrical Engineers, volume 88, part II, 1941, page 67.

E. Hansson and **S. K. Waldorf:** Mr. McCann has pointed out limitations of surge-crest-ammeter links when the links are mounted on lightning arresters. Obviously, serious errors can result if the records from such installations are used to estimate the number of arrester operations. However, if the data are used to determine the upper limits of current which arresters discharge under different operating conditions, the result is likely to be fairly reliable and useful.

Mr. McCann and Mr. Oldacre both point out that the surge impedance of an overhead ground wire, as given in section 9 of the paper, is not the theoretical surge impedance. The authors intended it to be only a practical factor derived from actual field data which, when applied to a crest current, gives a conservative estimate of the voltage to be expected at midspan. When used in this way, this factor is comparable with the d-c-tower footing resistance used with a crest current to determine tower potential.

Mr. Oldacre, in his comments on the performance of the Powerton–Crawford line, mentions a difficulty which is quite common in the operation of transmission lines—that of locating the point of flashover when tripout occurs. If the operation of lines is to be improved progressively, it is important that the location of such troubles be found. Flashovers are so rare on modern high-voltage lines, that their few remaining weaknesses will not be uncovered unless every fault is traced to its source. For instance, a tripout of line *A* mentioned in the paper occurred in 1943 during a lightning storm. Although experienced patrolmen supplied with information from automatic oscillographs and records of surge-crest-ammeter links on every tower could not find the point of flashover, the search was not given up. The patrolmen continued their examination of the line on their regular patrols, and the point of flashover was located some days later by the discovery of a chipped insulator. Apparently the sun had to be at a favorable angle for the spot on the insulator to be seen. This discovery was of some importance, because a flashover at this point revealed an unsuspected limitation of shielding.

Mr. McCoy asks four questions, the first one concerning the relative merits of deep-driven ground rods and a counterpoise system. Unless a conducting layer can be reached by ground rods of reasonable length, the counterpoise is to be preferred. The reason for this preference is that the authors have no knowledge of any experience with ground rods in deep sandy soil, whereas, there have been unpublished reports of satisfactory operation of the Boulder Dam lines. These lines traverse a dry and sandy desert, and the towers are grounded with a counterpoise system.

The second question asked by Mr. McCoy concerns the possibility of providing 220-kv lines with perfect lightning protection. It would be foolhardy to recommend steps that might be taken to provide 100 per cent efficiency of protection, because making such a recommendation would imply that everything is known about lightning. However, experience with line *A*, which has been operating for 12 years with two flashovers, gives some hint of how perfect operation might be approached more closely. The first lightning flashover on this line occurred in 1933 at a tower having a footing resistance of 25 ohms. This situation obviously could (and has been) taken care of by providing lower footing resistance. The second flashover of this line occurred in 1943 at a tower on the side of a hill having a slope of about 30 degrees to the horizontal. The work done on shielding by Wagner and McCann indicates that the shielding on the down-hill side of this line was neutralized partly at the point of flashover by the slope of the ground. This circumstance suggests a way in which a new line might be improved over the existing line, that is, by varying the position of the overhead ground wires on the towers to provide a uniformly high degree of shielding, no matter what the topography may be at any point. It must be borne in mind that occasional flashover might still occur, caused by some as yet unknown effects, even though the best present-day design information were utilized. In short, there is not yet sufficient information available about lightning to state with any certainty what is needed to obtain perfectly lightning-proof lines.

In answer to Mr. McCoy's third question, an average of 150 man-hours is required to install counterpoise on one mile of line. This, of course, varies with the number of conductors installed. The material costs 75 dollars per mile or more, depending on the kind used. The cost of adding overhead ground wires to lines cannot be estimated, except for specific structures.

Mr. McCoy's fourth question relates to the predetermination of lightning outages in territory where the lightning-storm frequency and severity are different from those on the system studied. A good approximation probably can be obtained if the calculation is made as outlined in the appendix of the paper, except that the stroke frequency should be corrected for differences in storm frequency as determined from Weather Bureau data. The average thunderstorm frequency on the transmission system, which is the subject of the paper, is 35 per year, with an average stroke frequency of 113 per 100 miles of line per year, as shown in Table II of the paper. For example, if the Weather Bureau data indicate a thunderstorm frequency of 25 per year on some other system, the stroke frequency used in estimating performance would be 25/35 of 113, or 81 strokes per 100 miles of line per year.

It is gratifying that Mr. Whitehead has been able to use some of the data given in the paper for judging the value of changes in the protection on a transmission line. It is hoped that, as time goes on, others will find the data useful.

Transmission and Relaying Problems on the Fort Peck Project

Discussion and authors' closure of paper 44-13 by Erik Floor, H. N. Muller, Jr., and S. L. Goldsborough, presented at the AIEE winter technical meeting, New York, N. Y., January 24–28, 1944, and published in AIEE TRANSACTIONS, 1944, May section, pages 209–14.

A. R. van C. Warrington (General Electric Company, Philadelphia, Pa.): I should like to congratulate the authors on their interesting paper describing the adaptation of the constant mho circle to the balanced beam type of relay, which I understand was applied at Fort Peck last summer.

These characteristics used by the authors for zone 3 have been used for zone 3 in the standard General Electric distance relay for about 15 years. They have also been used as zone 1 for many years in the *CEX* distance relay built for the Pennsylvania Railroad to distinguish between faults and heavy locomotive starting currents and, more recently, on the 270-mile Midian-Omaha line to prevent operation on power swings.

The advantage of the offset or constant mho circle is that its ohmic reach sideways on power swings is only half its ohmic reach on faults whereas the ordinary impedance relay operates on the same value for both conditions, that is, is twice as vulnerable to power swings.

I presume the reason why the authors did not use the offset circle for zone 1 was because their beam-type relay has zero torque on a nearby fault with substantially zero potential when the circle passes through the origin.

The authors state that this condition can be overcome by unbalancing the I^2 torques which means that, at very low voltage, the unit becomes a nondirectional instantaneous overcurrent relay. The directional action can be restored by means of an external directional relay which also will have zero torque at zero voltage unless provided with "memory action."

It would be interesting to prospective users to know the burden imposed by the potential circuit of these relays and how it varies with the current magnitude and phase angle.

W. R. Brownlee (Commonwealth and Southern Corporation, Jackson, Mich.): The transmission problems encountered in connection with the Fort Peck project bring up interesting questions on system planning. For the initial development described, the generator capacity is 35 megawatts, the capacity of the step-up transformer is 50 megavolt-amperes and that given for the step-down unit at the receiving end of the line is 60 megavolt-amperes, whereas the thermal ability of the transmission line is approximately 200 megavolt-amperes. This line is mentioned as the "initial" transmission, and since the present generator and line are unstable for all faults on the adjacent system which involve more than one phase, is it planned that two additional 161-kv lines, each 288 miles long, will be installed for the two future generators? If so, the reasons determining the choice of the present conductor size (477 thousand circular mils steel-reinforced aluminum cable) would be of interest.

The improved distance relay characteristics developed in connection with this long line installation should find wide application on shorter lines also. Certainly the constricted "mho" circle with only its radius rather than its diameter reaching toward the danger zone of swing current operation is a marked improvement over a theoretical circle of pure impedance. An immense improvement is provided over the conventional "impedance" elements in common use which have circles so distorted that the relays are about twice as sensitive to load swing current as to fault current. The general nature of this characteristic is mentioned in the authors' reference 1. It is therefore hoped that the new mixing transformer is suitable for the inexpensive conversion of existing "impedance" relays since in so many instances they fall so far short of developing the emergency capability of the protected transmission line.

The following questions are asked regarding the relaying features of the project:

1. What is the range of relaying speed at the Rainbow substation on which the stability studies are based?
2. The carrier-current application shows the use of the restricted circle even for the unit which initiates carrier transmission. In the case of medium length lines, would a simple impedance unit be as satisfactory?
3. What is the percentage of error by which the beam element deviates from a perfect circle at any point?
4. How does the accuracy of the beam element vary when the relay voltage varies from 10 per cent to 90 per cent?
5. Can the new mixing transformer be used for the inexpensive conversion of existing "impedance" relays?

J. G. Holm (Boston Port of Embarkation, Boston, Mass.): The authors present in their paper a very interesting case of a shunt reactor on the generator bus of a long single-circuit transmission line. As far as I know, this is the first case when the application of a shunt reactor to a transmission system has been reported in this country. A shunt reactor on the generator bus has been reported on a line in France[1] where its operation proved to be most satisfactory, but no such reactors are known to have been installed in this country.

It is gratifying to learn that the shunt reactor on the Fort Peck line permitted it to be synchronized at either end, lowered the voltage rise inherent to the line to three per cent from the 31 per cent obtained for the case with no reactor, and increased the line steady-state and transient-stability limits.

Several thoughts, however, arise in the direction of further improvement of the effectiveness of the shunt reactor and of the operation of the Fort Peck line.

The percentage increase in the stability limits in system *b* over system *a* is approximately the same for both the transient- and the steady-state limits. Study of the shunt reactor shows that when its kilovolt-amperes and those of the generator are properly coordinated the steady-state stability limit increases in proportion more than the transient-stability limit. If this was not obtained on the Fort Peck line, it is probably because the shunt reactor appears to be somewhat too big in comparison with the one generator presently installed. In the future when a second generator will be installed at Fort Peck the two stability limits of the line will raise considerably, and the steady-state limit in all probability will raise more than the transient limit. The shunt reactor increases the system excitation voltage more than it increases the total system transfer reactance, the two factors acting in opposite directions insofar as stability limit is concerned. The resulting increase obtained in the stability limit is accompanied by lowering the power factor at the generator bus. And since the generator must supply the line with the same amount of active power as when the shunt reactor is not connected, additional generator capacity is required to accomplish this, and to do it without undue overheating the generator. This is why the contribution of the shunt reactor to the increase of system stability limits will be considerably greater when the second generator is installed at Fort Peck than is the case at present. The proper size of the shunt reactor to be installed as well as the required increase in generator kilovolt-amperes, may be calculated from the ratio of the total system transfer reactance without the shunt reactor on the bus to that with it.[1]

The second generator at Fort Peck will permit the system operation at a level above 85 per cent of the steady-state limit, it will probably permit a safe sailing through a double-line-to-ground fault at the Rainbow substation, and perhaps it will maintain stability when a line-to-ground fault occurs at the sending-end of the line.

With the kilovolt-amperes of the shunt reactor and the generator not quite in balance, it seems off-hand that had the 15,000 kilovolt-ampere reactor been installed on system *c* the system stability condition would be somewhat better than that obtained at present on system *b*, because of the smaller charging kilovolt-amperes required by system *c*.

As long as the Fort Peck line has to operate with one generator only, its operation under fault conditions could be improved by using high-speed circuit breakers jointly with carrier-current relaying, and especially by employing single-pole reclosing switching.

It is hoped with the authors that the use of shunt reactors on the generator bus of long transmission lines will increase, for the benefits derived from them are very great indeed.

Reference

1. Stability Study of A-C Power Transmission Systems, J. G. Holm. AIEE Transactions, volume 61, 1942, pages 893–905.

H. N. Muller, Jr., and S. L. Goldsborough: The authors wish to thank J. G. Holm for further evidence confirming the application of large shunt reactors to electrically long high-voltage transmission systems. His comments render an interesting discussion of the optimum proportions of a shunt reactor to gain a maximum in power limits. The authors did not intend to infer that the Fort Peck shunt reactor application was the first such installation to be affected in this country. The use of such a device on transmission lines has been previously reported in the United States.

High-speed single-pole reclosing was not considered for the Fort Peck 161-kv line since existing power utilization did not require the utmost in service continuity. It was necessary, however, to consider stability for faults on the 105-kv system of the Montana Power Company since there is a multiplicity of lines connected to the receiver bus, and instability resulting from faults on these lines would render the interconnection entirely too undependable.

Mr. Brownlee has raised several questions with respect to transformer and transmission facilities. It is unusual in appearance that the generation, step-up and step-down transformers, and line capacities should be so divergent. This condition results from the fact that the two banks of large power transformers were installed, and the 161-kv transmission line was built, originally to furnish power from Rainbow substation to Fort Peck for dam construction. Therefore, entirely different circumstances dictated the choice of power transformer sizes than now exist with generation installed at Fort Peck. The line conductor selected is consistent with modern practice regarding minimum conductor size for low-loss operation at 161 kv.

At the present time it is not possible to say whether the existing transmission circuit will ever be paralleled with additional circuits. This will be determined by the load growth in the general area at such time as ultimate generation is installed.

Mr. Brownlee has raised some questions concerning the relay characteristics. The range of relaying speed at the Rainbow substation on which the stability studies were performed is of the order of 25 to 35 cycles.

The displaced circular characteristic was shown on the carrier starting element because the conditions obtaining on long lines were under consideration. However, if medium length lines are to be considered, it is, of course, obvious that a simple impedance element is satisfactory since such an element for initiating carrier has been in use for almost ten years.

The deviation of the beam element characteristic from a perfect circle is of the order of plus or minus five per cent. The accuracy of the beam element under voltages from ten per cent to 90 per cent is ten per cent and varies in the same manner as the impedance curve of the standard impedance element with a flat-phase-angle characteristic.

Mr. Warrington states that the so-called "mho" characteristic which has been in use for a number of years is one half as sensitive to power swings as it is to faults. This emphasizes the advantage of the circular characteristic described which can be adjusted so that a ratio of response to fault ohms to response to power swings much greater than 2 to 1 is obtained. This is done by shifting the circle toward the *X* axis and then insuring operation on close-in arc faults by shifting the circle to include the required portion of the *R* axis. This method moves the circle away from the power swing vector but still encompasses the fault vector. This has not been possible, heretofore, in relays using the displaced circular characteristic because the circle has always had to pass through the origin.

The volt-ampere burden consumed by the voltage coils is approximately three volt-amperes. This burden is substantially constant and does not vary with the current because of a suitable series potential circuit impedance.

Fusing Practices on Distribution Systems

Discussion and authors' closure of paper 44-52 by John S. Parsons and J. M. Wallace, presented at the AIEE winter technical meeting, New York, N. Y., January 24–28, 1944, and published in AIEE TRANSACTIONS, 1944, March section, pages 89–91.

E. G. Newton (General Electric Company, Pittsfield, Mass.): The data on the fusing practices on distribution systems as compiled and presented by Parsons and Wallace give a good over-all picture of existing practices and problems. However, in discussing the weighted average fusing ratios of their Figure 3 they state, "The ratio of the minimum melting point of the fuse link to the full-load current of the transformer will be twice that shown in the figure, as all these data are based on *N*-rated links." Then they compare the earlier data shown by the dotted lines of the figure with the solid-line data which the authors indicate they corrected on the basis of the foregoing statement. This leads to their conclusion that "One factor probably contributing to the increased fusing ratio is the change in method of rating fuse links. This automatically would increase the fusing ratio if the use of the same link ratings were continued."

A check of the published time–current characteristics of the most popular types and makes of distribution fuse links would indicate that probably as high as 85 to 95 per cent of the *N*-rated 1-to-50-ampere fuse links in use today melt at approximately 150 per cent of their rating within five minutes (300 seconds). The *N*-rating standards permit this as they specify that the fuse links must "melt at not over 230 per cent of the rating within 300 seconds."

Therefore the shift in fusing practices during the past ten years as indicated by Figure 3 of the paper may be more a result of incorrect interpretation of the data than a trend in actual practices. However, the conclusions drawn by the authors are a direct indication as to what will happen if an industry shift is made so that all fuse links for distribution cutouts melt at 200 per cent of the rating in 300 seconds and the same fuse-link ratings used at transformer installations. Therefore, if such a change is made in industry standards, an engineering approach to the fusing on distribution systems will require restudying all present operating practices in order to maintain the existing balance between the protection provided the transformer and the unnecessary blowing of fuses by short-time current surges or lightning.

G. Fred Lincks (General Electric Company, Pittsfield, Mass.): The authors have furnished some very interesting data on the fusing of distribution systems operating below five kilovolts. About 1939 a similar check with 110 utility engineers in all parts of the country showed fusing practices as indicated in Table I of this discussion. These data check fairly closely with the data compiled by the authors with the addition of information on 6,900-volt systems. However, in interpreting the data, such as in Figure 3 of the paper, the authors appear to have introduced errors based on a misconception of the more usual design practices under the *N* rating basis for fuse links.

One point which seems to have been overlooked by the authors, but which shows up in similar data obtained by the General Electric Company and is quite noticeable in Tables I, II, and III of their paper, is the lower fusing of transformers rated 25 kva and larger employed by a considerable percentage of companies reporting. The greater investment in the larger transformers makes improved protection desirable. The diversity of load makes fuse blowing by surge currents less likely. The reduced lightning blowing of the higher-rated fuses combines with these other conditions to justify such a practice. However, with such lower fusing, care must be exercised in choosing fuse links which will carry safely normal load and short-time overloads on the transformers without overheating the fuse cutouts.

The authors express a hope for fewer ratings. Caution should be exercised in any action in the direction of fewer ratings, lest the ability to get the number of different sizes for connecting several fuse links in series at line sectionalizing points be impaired. Also, any curtailment in the number of ratings should be studied carefully as to the effect on the availability of ratings for the uniform fusing of all voltage ratings of transformers. For example, in Table I of this discussion the 5- and 7.5-kva 6,900-volt transformers could not be fused "at one-third ampere per kilovolt-ampere," as there is no suitable fuse-link rating in the 19 different sizes from 1 to 100 under the *N* rating basis.

The higher fusing of the lower kilovolt-ampere-rated transformers as shown by the data has been instituted with a direct over-all benefit by some companies, whereas

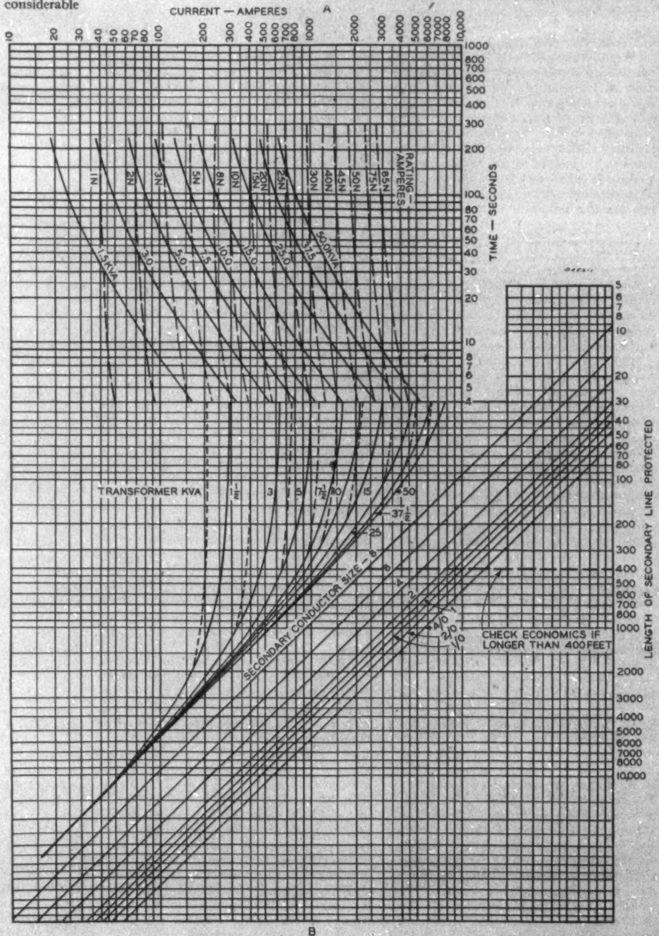

Figure 1. Charts for determining lengths of secondary line for which fuses will provide short-circuit protection to transformers

A. Time–load curves of distribution transformer in terms of secondary current. Dotted curves show universal cable-type fuse links plotted at a 20:1 ratio for use on 2,400-volt circuits

B. Chart for determining length of secondary line protected others have gone to it reluctantly as the best balance obtainable between protection from secondary short circuits and unnecessary fuse blowing by lightning. Particular circumstances imposed by their specific circuit conditions or the prevalence of difficulties caused by acts of nature make lower fusing desirable from the standpoint of protecting the transformer. As pointed out by the authors, this divergence of operating requirements makes it desirable to combine some engineering analysis with operating experience in deciding on the best fusing practice for a given circuit. The availability of American Standard time–load curves for transformers makes it possible to determine quite readily how far out on the secondary lines primary fuses will provide protection against faults on the secondary lines, as well as when and how much additional benefit can be obtained by the use of secondary fuses. The charts in Figure 1 of this discussion permit doing this by transferring the point of intersection of the dotted fuse-link curves with the solid line American Standard transformer time–load curves of chart *A*, to the curved lines of chart *B* for 4/0 secondary conductors, and for other sizes of conductors transferring again to the 45-degree lines. From the point thus located the length of secondary line protected by the fuse can be read directly from the scale at the right. The charts of Figure 1 are a practical contribution toward the solution of the problem presented by the authors. These charts are being presented elsewhere to the industry with a more detailed discussion and instruction and on paper corresponding to the log–log paper standardized by Edison Electric Institute and National Electrical Manufacturers Association for publishing the time–current characteristics of fuse links, with detailed instructions for determining the zone of secondary short-circuit protection provided by both primary and secondary fuse links.

REFERENCE

1. FUSING DISTRIBUTION TRANSFORMERS, G. Fred Lincks. *Distribution*, April 1944, page 6.

Ralph H. Earle (Line Material Company, Milwaukee, Wis.): This paper contains information of considerable interest to manufacturers and users of primary fuse cutouts and links. Several points warrant comment.

As to the minimum size of fuse link adopted as standard by various operating companies as indicated in Figure 1 of the paper, it is interesting to see that so many companies use one-ampere and two-ampere links on 2,400- and 4,800-volt transformers, and also that on an appreciable number of the systems, four or more fuse links are used in cascade for sectionalizing. It was our belief until reading this paper that both of these practices were limited largely to 7,200/12,500- and 13,000-volt rural systems with extended lines and low-capacity transformers.

One point on which further information would be of considerable value is the service record to be expected when various minimum-size links are adopted as standard by an operating company. For example, assume that a company has adopted a two-ampere link as the minimum and is experiencing an objectionable number of unexplained outages. If the operator should raise this minimum amperage of link to five or ten, for example, to what extent would the number of unexplained fuse blowings be reduced?

Another example: suppose that a company is now using a 15-ampere link as the minimum size but would prefer to adopt a lower amperage in an effort to obtain some degree of overload protection on the transformers. How low can the amperage of links be before an excessive number of unexplained fuse blowings occurs? Information of this kind is of value to an operator contemplating such a change.

In regard to rupturing capacity of primary fuse cutouts, the paper indicates that a large number of operators are using fuse cutouts on lines whose fault current exceeds the rated rupturing capacity of the cutout. This seeming inconsistency is explained by a consideration of the manner in which cutouts are rated.

Under industry standards, approved by NEMA and EEI, a cutout is required to rupture its maximum rated current five times and still be in operating condition. The ratings specified in the standards are based on rigorous rupturing-capacity tests during which the cutout is called upon to rupture successively higher and higher currents until some form of damage to the cutout occurs. This damage may be one of several forms as described in the following:

1. The recoil may be so great as to tear the cartridge loose from its mounting and thus render the cutout unsuitable for further service. Damage of this kind is seldom a function of the number of shots; if a cutout will rupture a certain amperage of fault current once without damage from recoil, no damage from recoil is likely to occur on future shots.

2. Another form of damage from excessive current is caused by flashover of the cutout due to large amounts of hot conducting gases being expelled from the open end of the cartridge. Resistance to this form of damage is built into the cutout by providing free venting of these gases. Here again, the damage is not a function of the number of shots.

Table I. Data on the Fusing Practice of 110 Utility Companies

Transformer Rating—Kva	Fusing—Times Full-Load Current of Transformer							Actual Fusing—Amperes						
	1.5	1.3 to 1.8*	2	Amperes° Per Kva	3	4	5 or More	1 Ampere	2 Amperes	3 Amperes	5 Amperes	8 Amperes	10 Amperes	15 Amperes
Per Cent of 110 Utilities Reporting on 2,300/4,160 Wye Fusing Practices														
1½		4.10	4.10	41.30		13.4	41.20	4.10	41.3	13.4	24.8	2.04	10.3	1.00
3		1.9	1.9	47.7		30.9	19.5		1.9	47.7	30.9	1.86	10.3	4.60
5	1.9	1.9		62.3		6.4	29.4			1.9	62.3	6.4	21.1	5.5
7½	4.6	4.6		38.0	41.6	10.2	5.6			0.9	3.7	38.0	41.6	10.2
10	1.9	2.8	0.9	68.5	20.4		8.3				1.9	.9	68.5	20.4
15	4.2	4.2		62.2	21.0	9.3	3.3						4.2	62.2
25	2.7	11.9	9.2	72.5	7.4		8.2							2.7
37½	6.6	17.0	10.4	60.5	17.9	2.8	1.8							
50	4.7	19.5	14.8	61.1	15.7		3.7							
75	13.8	23.6	9.8	62.7	12.7		1.0							
100	19.6	19.6		69.1		10.3	1.0							
150	22.6	25.8	3.2	71.0	3.2									
200	39.0	39.0		61.0										
Per Cent of 44 Utilities Reporting on 6,900-Volt Fusing Practice														
1½				‡4.7			95.3	16.7	16.7	16.7	38.1		7.1	
3	‡4.8	‡4.8		23.8		21.5	49.9	23.8	21.5	14.2	28.5		7.2	
5	7.4	7.4		X	29.2	19.5	43.9	7.4	29.2	19.5	36.5		7.4	
7½		21.2	21.2	X	35.8		43.0		21.2	35.8	35.8		7.2	
10	3.9	3.9	35.3	53.0			7.8		3.9	35.3	53.0		7.8	
15	2.4	2.4		71.4		11.9	14.3			2.4	71.4	11.9	11.9	
25	4.7	4.7	27.5	X	47.5	15.6	4.7				4.7	27.5	47.5	15.6
37½			31.8	53.6		9.8	4.6						31.8	53.6
50			61.5	20.5	12.8	2.6	2.6							61.5
75	5.3	29.0	23.7	51.8	5.3	5.3	2.6							5.3
100	2.7	23.7	21.0	34.2	18.4	18.4	5.3							
150	8.0	32.0	28.0	40.0		8.0	16.0							
200	5.0	25.0	20.0	35.0	20.0	20.0								

* This combines the percentages for fusing at 1.5 times full-load current of the transformer with that portion of the percentage for fusing at 2 times where one-size-smaller fuse link than 2 times is used.

‡ Two utilities use one-half-ampere fuse link with 1½-kva transformers. One uses one-half-ampere and the other three-fourths-ampere fuse link with three-kilovolt-ampere transformers.

° One ampere per kilovolt-ampere rating of transformer at 2,300 volts. One-half ampere per kilovolt-ampere rating of transformer at 4,800 volts. One-third ampere per kilovolt-ampere rating of transformer at 6,900 volts.

X No fuse size available so 3 times fusing is employed.

3. The third form of damage from excessive current is severe erosion of the fiber cartridge and does depend upon the number of shots. It is inherent in the operation of an expulsion cutout that fiber be eroded from the bore of the cartridge on each shot. The amount of fiber eroded is directly a function of the amperage of the fault current and the number of shots. If the cutout is called upon to rupture excessive currents repeatedly, erosion may proceed to the point where the tube will burst due to internal pressures of the hot gases, or the bore may be enlarged to the point where the cartridge will be unable to interrupt small fault currents satisfactorily.

Since erosion of the cartridge is so definitely involved in the number of shots, the industry standards limit the maximum value of fault current to a value that permits the cutout to operate five times and still be in condition for further service.

The result is that in general the modern primary fuse cutout has a single-shot rupturing capacity very much in excess of its nominal rated rupturing capacity.

John S. Parsons and **J. M. Wallace:** The discussion presented by Mr. Earle asks a question about the relationship between fuse-link size and the number of unexplained fuse operations. The question is quite pertinent, and quantitative data undoubtedly would assist in standardizing practice. It is the authors' opinion that increasing the fuse-link size invariably will lead to fewer outages. This opinion is based on studies showing that with increasing values of surge currents, the frequency of occurrence becomes less. As the surge-current capacity of fuse links increases with increase in ampere rating, the conclusion is evident. Since fuses will not provide overload protection for transformers, the authors believe that a marked increase in the fusing ratio will result in little or no increase in the rate of transformer burn-up.

In addition to the factors mentioned by Mr. Earle regarding the interrupting capacity of cutouts, it should be remembered that the actual fault currents seldom reach the calculated values on circuits of this voltage because of fault impedance. This factor helps to permit the satisfactory use of cutouts at locations where the calculated fault currents are in excess of the interrupting rating of the cutout.

It is apparent that Mr. Lincks has overlooked Figure 2 of the paper where the lower fusing ratio used on larger sizes of transformers has been graphically presented. The reasons for this lower ratio are well brought out in his discussion.

The wide variations now found in fusing practice indicate that the necessity for uniform fusing of all transformers is not so imperative as Mr. Lincks seems to feel. The authors believe that the uniform fusing of all sizes of transformers is unnecessary and that the elimination of fuse-link ratings below five amperes would result in improved performance on distribution systems.

The discussion by Mr. Newton is confined principally to the question of the melting point of *N*-rated fuse links. While it is true that a number of the present-day fuse links, designed to the *N* standards, melt in five minutes at a current of 150 per cent of rating, there is only one manufacturer who does not offer a link which melts at 200 per cent, as assumed in the paper. As the 200 per cent links are the more recent design, it is felt that most operating companies prefer this type. If it were possible to average the links now in service on the basis of their actual melting currents, the shift would be somewhat less than that shown in Figure 3 of the paper. As can readily be seen, the exact shift is subject to argument on the basis on the data now available.

Reclosing of Single Tie Lines Between Systems

Discussion and authors' closure of paper 44-30 by W. W. Parker and H. A. Travers, presented at the AIEE winter technical meeting, New York, N. Y., January 24–28, 1944, and published in AIEE TRANSACTIONS, 1944, March section, pages 119–22.

John Grzybowski Holm (Boston Port of Embarkation; Boston, Mass.): In their paper the authors made a distinct contribution by pointing out the advantages single-pole switching has over the three-pole switching. The practical advantage of single-pole switching is evident particularly when high-speed reclosing breakers are employed in a single-circuit line. The application is by no means limited to this case, and may be used with equal success on multicircuit lines where the reclosing principle is not applied.

Additional comments on the part of the authors will be welcome, however, on the question of single-pole switching of lines where the high-voltage winding of transformers at one or both ends of the line is grounded through a resistance or an impedance of higher value.

The definite advantage presented by illustrating the paper with Figure 4 is unquestionable. The second part of the paper containing Figures 5, 6, and 7 given for the selection of suitable single-pole or three-pole switching presents an entirely different picture.

Closer inspection of these curves raises questions based on the lack of their uniformity and places in doubt the advisability of their use for the purposes intended. The curves show that insofar as the transient-stability limit is concerned, the degree of advantage of single-pole over three-pole switching changes with the ratio of sending-end to receiving-end generator capacity, with the reclosing time and with the measure of the electrical length of the line $\left(\frac{\text{miles}}{\text{kv}^2}\times\text{base}\right)$. The advantage obtained is very irregular and inconsistent. For example, it follows from Figure 5 that, at 20-cycles reclosing time and with the measure of the electrical length of the line increasing from 0.4 to 0.9, the advantage of single-pole switching over three-pole decreases uniformly from 41 to 3 per cent; whereas at 30 cycles reclosing time, and at the same limits of variation of the electrical length of the line, this advantage changes from 65 per cent to 67 per cent, increasing to 78 per cent at 0.6 value of the electrical length of the line.

When curves of Figure 7 are studied, it will be seen that, with the variation of the measure of the electrical length of the line from 2.0 to 7.5 at 20 cycles reclosing time, the advantage of single-pole over three-pole switching increases from 21 to 25 per cent, or remains practically constant; whereas at 30 cycles reclosing time the advantage single-pole switching has over three-pole switching drops rapidly from 74 per cent to a mere 15 per cent at 7.5. In other words, at 20 cycles reclosing time the increase in the transient-stability limit obtained by employing single-pole switching is of the order of 21 to 25 per cent, remaining practically constant at all values of the electrical length of the line; whereas at 30 cycles reclosing time the increase in the transient-stability limit with single-pole switching over that obtained with three-pole switching is 74 per cent when $\left(\frac{\text{miles}}{\text{kv}^2}\times\text{base}\right)$ equals 2.0, and the increase is only 15 per cent at 7.5.

This lack of uniformity for which the systems themselves do not appear to give any obvious reason is more apparent when the power transmitted is plotted against the inertia constant of the sending-end rotating machines.

It is possible that the irrelevant inconsistency of data given on Figures 5, 6, and 7 is due to the fact, emphasized in the paper, that, when passing on the network analyzer from one system to another, the authors determined the transient-stability limit by varying only the length of the line with all the other conditions held constant. Thus, in some cases the transient-stability limit was obtained for systems whose voltage was too low (or too high) for the line's length; and the curves were plotted through points obtained for lines whose voltage was natural for the line's length, as well as through points obtained for lines operating at a voltage inconsistent with either their length or the magnitude of the block of power transmitted. The transient-stability limits thus were artifically depressed (or lifted, if lines were studied on the network analyzer in the reverse direction).

Whatever may be the reason, it seems doubtful that the curves of Figures 5, 6, and 7 can be used in order to determine with sufficient reliability the type of switching for systems with proper relationships between the distance of transmission, the voltage, and the block of power transmitted.

C. E. Parks (Public Service Company of Indiana, Inc., Indianapolis, Ind.): This paper presents a fine solution to a very critical problem, especially critical in these days of heavily loaded transmission systems and generating equipment. Any improvement that can be made in system operation before, during, and immediately following system faults is sure to be appreciated greatly by the operating men of most utilities. Single-pole relaying and reclosing seem to be an excellent answer to the stability problems that confront most system operators today.

However, it was a disappointment to note that the authors failed to mention that the employing of single-pole relaying and reclosing is nearly as effective when applied to the several ties which may exist between systems as it is when applied to single-circuit ties. The same advantages hold true in both cases, namely:

1. It reduces the shock to the system from a voltage-fluctuation standpoint.

2. It materially assists in the preventing of sudden load shifts in the transmission ties paralleling the line under fault.

The protection of parallel lines by single-pole relaying and reclosing, an application which has not yet been investigated fully, seems to offer further advantages to be derived from the use of this relay scheme, particularly if such parallel lines have an operational record which indicates a high percentage of double-circuit faults.

Our "field tests" on this relay system have proved conclusively that much is to be gained in system performance by the use of this type of line protection. It is our hope that the manufacturers of this equipment will strive continually to improve and simplify it.

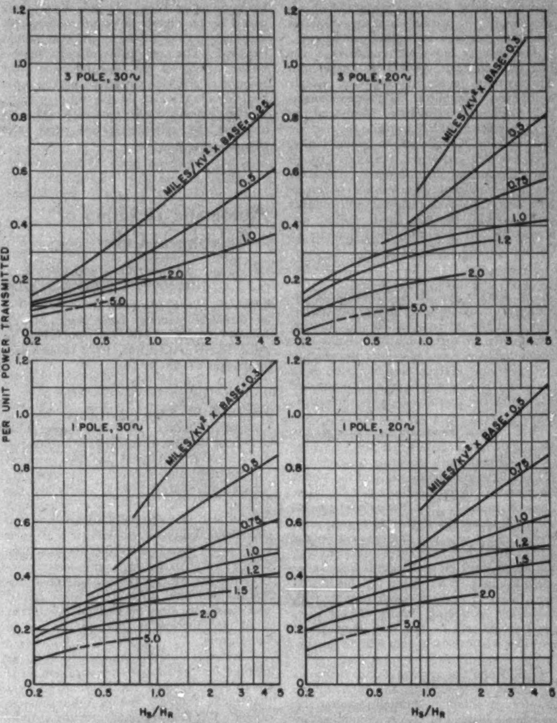

Figure 2. Transient-stability limit for reclosing of a single tie line between two systems for various ratios of sending-to-receiving-system inertia

Base equals receiver-system generator capacity, and per unit power is referred to this base. Fault is double line to ground cleared in nine cycles. Points falling below curves indicate stability

H. N. Muller, Jr. (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The paper is particularly timely. Two years ago the papers cited as references 2 and 3 in Parker and Travers' paper were presented and were the first published works dealing extensively with high-voltage single-pole switching. The first application of high-speed single-pole reclosing went into service less than three years ago, and since then several other applications have been effected on single-circuit interconnecting tie lines. It is expected that such application will be accelerated further when material restrictions become less severe.

The body of the Parker and Travers paper presents a succinct but clear summary of the fundamentals of transient stability as applied to and affected by high-speed reclosing. The most valuable contribution made is the data summed up in Figures 5, 6, and 7 in the paper. This is the first set of comparative curves arranged in a form that is easy to use and based upon a great multiplicity of very practical solutions. Nearly any case in which the system involved can be reduced to the simple two-machine stability problem will render an answer sufficiently accurate for a reliable guide to reclosing requirements.

It occurred to the discusser that interpolation between Figures 5, 6, and 7 in the paper would be necessary if the data in these three sets of curves were to be applicable to a really large percentage of the cases arising in practice. This is true, because three specific ratios of sender-to-receiver inertia are plotted, and few practical cases would have inertial relations corresponding exactly to those three combinations chosen.

With this idea of interpolation in mind, the discusser took several actual cases recently calculated and attempted a quick check by using the general curves of Figures 5, 6, and 7. Those cases, where the sending- and receiving-end inertias equalled or even were close to the ratios used in the Parker and Travers curves, gave checks corresponding closely to calculations. In other cases where the two values of inertia differed greatly from those used in the curves, difficulty was encountered in trying to interpret the interpolation. For example, in the actual system of Figure 1 of this discussion, it was desired to check, by the method presented, the selection of high-speed reclosing. Note that the ratio of sender-to-receiver generation and inertia is 2.56, which figure in about midway between the values upon which Figures 5 and 6 of the Parker and Travers paper are based. If Figure 5 is used, the solution indicates that 20-cycle single-pole reclosing is dictated to maintain stability. If Figure 6 is used, the solution indicates much greater stability, permitting the use of any but 30-cycle three-pole reclosing. This leaves the engineer without a dependable answer, since interpolation could not tie down the solution to any minimum applicable reclosing measure.

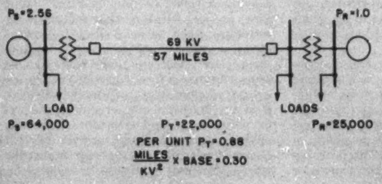

Figure 1. Typical system for which type of reclosing is to be selected

To relieve this difficulty and to broaden greatly the applicability of the work presented by Parker and Travers, the results of their Figures 5, 6, and 7, plus a little additional data, were cross-plotted into the curves of Figure 2 of this discussion, and since the ratio of sender to receiver inertias is now a continuous function, reliable interpolation becomes possible. Using the same example, one can see that three-pole 30-cycle reclosing is definitely unstable, whereas either single-pole 30-cycle or three-pole 20-cycle reclosing is stable, and has a reasonable margin. The uncertainty is relieved, and dependable solutions are obtained, regardless of the variable nature of inertia constants between individual problems.

Also worthy of special emphasis is the fact that *all* cases studied by Parker and Travers use a double line-to-ground fault as a criterion of stability. High-speed single-pole reclosing was advanced because of the large increase in stability during the clearing and reclosing of single line-to-ground faults, a type which comprises a majority of all transmission-line disturbances. The fact that a very substantial advantage of single-pole over three-pole reclosing still obtains with double line-to-ground faults undoubtedly will give additional impetus to the application.

W. W. Parker and **H. A. Travers:** H. N. Muller's discussion is an important contribution to the information on this subject. The family of curves plotted as per unit power versus ratios of inertia of the sending and receiving system correlate the three basic variables in such a way as to increase materially the usefulness of the investigation.

C. E. Parks' discussion in regard to the use and performance of single-pole switching on multicircuit lines is greatly appreciated. We intend to extend our study of the reclosing problem to include such applications. There is no doubt that the general conclusions will be as Mr. Parks points out; however, it is not expected that the percentage gain of single-pole over three-pole switching will be as great on multicircuit lines as on single tie lines. The reason for this is that the relative increase of power transfer with single-pole switching during the de-energized period will be less with multicircuit ties than when there is only one tie line between two systems.

J. G. Holm has requested comments on the effect of grounding the high-voltage transformer windings through resistance or impedance instead of solidly grounding the neutral. This not only affects the fault condition but also the power transfer during the de-energized period. In general, high values of neutral resistance or impedance should not be used if the full advantage of single-pole reclosing is to be obtained. As far as Figures 5, 6, and 7 are concerned, linearity in the percentage gain of single-pole over three-pole switching should not be expected. The transient-stability limits are determined by a large number of factors as illustrated in the first part of the paper. It is impractical to determine the relative effects of these factors except by the detailed step-by-step method which was used in the determination of the curves. The shape of the curves is characteristic of this type of system operation and for the assumed conditions. The curves tend to have a steeper slope for the shorter distances, partly due to the fact that here the fault tends to accelerate both sending and receiving systems. The improvement of single-pole over three-pole switching is greater for the shorter distances, as considerable more power is transferred during the de-energized period than when the distances are great. This will be made clear by referring to Figure 8A of the paper; the longer the distance of transmission the higher will be the positive, negative, and zero sequences so that for extremely long distances the power transfer approaches zero. At this limit there would be no difference between three-pole and single-pole switching for the same reclosing time. The assumption of unity-power-factor loading of the line determines the amount of charging capacity and the practical voltage at which the line would operate, so that whenever the tie-line length was changed the relative factors were also changed in proportion.

Automatic Ticketing of Telephone Calls

Discussion and author's closure of paper 44-49 by O. A. Friend, presented at the AIEE winter technical meeting, New York, N. Y., January 24–28, 1944 and published in AIEE TRANSACTIONS, 1944, March section, pages 81–8.

Frederick L. Kahn (Automatic Electric Company, Chicago, Ill.): It is good news to hear of the first installation of automatic-toll-ticketing equipment in this country. The methods and problems have been presented and analyzed very clearly by the author. The high saturation of telephones in and near all metropolitan areas and the close community of interest within such areas make direct subscriber-to-subscriber dialing very desirable; the automatic-ticketing equipment provides a most important and final link. It is to be hoped that conditions after the war will permit the rapid introduction of this new feature in many areas.

A universal numbering system such as adopted for the Los Angeles area has some advantages, at least for those subscribers that make frequent use of interexchange calling. But it also complicates the switching equipment in several ways:

1. By necessitating special features such as the code reconstruction.
2. By requiring additional local switching equipment in the smaller suburban exchanges.

It is also to be wondered whether suburban subscribers should be burdened with dialing six or seven digits on all local calls when only four or five digit numbers are now required.

As and when automatic ticketing is more widely introduced, a point will be reached where universal numbering no longer can be carried through. This may be a blessing in disguise. A nonuniversal system employing a prefix for access to the toll-switching system permits simplification of the ticketing trunks, the senders, and the identifiers, as well as more economical and efficient trunking within each exchange and between exchanges. It will be interesting to see which course will be taken in the various applications of this equipment.

The ticketing equipment described in this paper is evidently designed to be fitted into an existing step-by-step system as an appendix or applique unit. In the future, we can expect new dial exchanges to be designed from the start with the possibility of automatic ticketing in mind. This probably will simplify and reduce the equipment for automatic toll ticketing and may also allow more extensive use of the senders and other special units on nonticketed calls.

The ratio of interexchange calls to local calls is much larger in the suburban exchanges than in the various offices of the nucleus city exchange. Hence automatic ticketing probably will be introduced first in the suburban exchanges and will be extended to the city subscribers later. When this stage is reached, adaptation of panel and crossbar systems to this new feature will be required. We have just heard of the crossbar toll-switching system. Is it assuming too much to think that this modern toll-switching system also provides facilities in this direction that have not yet been disclosed?

The mechanical production of the completed toll ticket is a great accomplishment. For the application at Culver City and with extended service rates, the recording of message units may be sufficient and satisfactory. In the long run, it will be desirable to go still further and to compute the total charges in dollars and cents automatically at the time the ticket is printed. This feature has been incorporated in an automatic-ticketing system designed by Automatic Electric Company. This system has been described in the *Strowger Technical Journal*, published by Automatic Electric Company (issue of June 1940).

J. E. Ostline (Automatic Electric Company, Chicago, Ill.): The automatic-ticketing arrangement for use in dial exchanges described in this paper again proves the flexibility and possibilities inherent in present-day telephone switching apparatus.

It would be interesting if the author could disclose some of the routines used in connection with the message ticketers. How often have the tickets to be removed and the inking facilities changed?

Under the heading "Nonticketed Calls," an interesting reference is made to using "trunking features" of the ticketing equipment for nonticketed calls. Does this refer to the use of the translation facilities in the sender, or some other facility inherent in the ticketing equipment? In a network of the size described, there are, no doubt, many instances where the use of senders or similar facilities could produce economies in local trunking and switching equipment.

The introduction to the public of automatic toll ticketing of the type described in this paper is another milestone in the history of dial-switching telephony. The author is to be congratulated for his very instructive and informative paper.

O. A. Friend: Mr. Kahn's comment on numbering plans emphasizes their basic relation to systems design. It is true that the equipment would be simplified and dialing effort on local calls somewhat reduced if the subscriber could be relied upon to prefix a directing code of one or several digits to those calls which are to be ticketed. In several instances directing codes are in use in metropolitan areas in the Bell System. However, the list of offices requiring the prefix varies in general from one originating office to another, and may vary for different classes of subscribers in the same originating office. Universal numbering as adopted for

Los Angeles permits dialing the called number as listed in the directory without regard to the point within the area from which the call is being made; it is especially desirable where the exchanges are not separated by definite geographical boundaries readily carried in mind. While each area presents individual problems, Bell System studies point toward gradual expansion of the universal numbering area of a metropolitan region with the increasing community of interest between the city and its suburbs, as offering over-all service advantages to telephone users.

With regard to the relationship of the crossbar toll-switching system to automatic ticketing, the toll system has the basic features which permit routing through it toll calls dialed by subscribers served by local step-by-step, panel, and crossbar offices where such routings may be economical.

The advisability of showing total charges on the ticket, either in terms of money or of message units, has been considered, but it is thought that this can be best handled as an accounting procedure. The ticket now shows the message units for the initial interval; this charge varies with the originating office and is provided readily during the setting up of the call by the equipment used for determining the routing. About four fifths of the calls terminate within the initial interval, and for such calls this is the total charge. The charge for overtime, on the other hand, must be determined after the call is finished; it follows a pattern determined by the initial rate and length of conversation. Making this computation an accounting procedure, to be performed automatically when the volume of tickets warrants mechanization of the accounting processes, avoids the need of providing equipment for performing this function in a large number of central offices.

In answer to Mr. Ostline's question, the tickets at present are removed daily, the interval being determined by convenience in fitting the accounting procedures. Inking is done once a week, and replacement of paper rolls at somewhat longer intervals.

The statements in the paper regarding the use of the trunking features of the system on nonticketed calls refer to the routing, over the trunk groups provided primarily for ticketed calls, of other calls dialed by operators or by classes of subscribers whose rate schedules do not require tickets on calls to the offices in question. Such calls which it may be economical to route over these trunks make use of the code reconstruction and translation features of the ticketing system.

Crossbar Toll Switching System

Discussion and authors' closure of paper 44-42 by L. G. Abraham, A. J. Busch, and F. F. Shipley, presented at the AIEE winter technical meeting, New York, N. Y., January 24-28, 1944, and published in AIEE TRANSACTIONS, 1944, June section, pages 302-09.

Gilbert Sorber (nonmember; Automatic Electric Company, Chicago, Ill.): It is most interesting to note the great amount of progress in and recognition of the application of automatic-switching principles to the switching of toll lines, and to the adaption of key-control operators equipment placed in service in Philadelphia. The paper quite comprehensively presents a picture of the basic switching principles and the technical problems involved. It appears, however, that the equipment is limited to major toll centers, wherein panel or crossbar equipment is employed for local services. The paper deals extensively with crossbar switching mechanism. This, we assume, is because it discusses the application of a particular manufacturer's type of equipment, and is not a controlling factor in arriving at the desired switching accomplishments.

We are greatly interested in the furtherance of automatic toll switching equipment for domestic use as well as for toll networks in the foreign field. The paper, because of the necessity of covering the subject in a general or skeletonized form, leaves us with a certain amount of doubt concerning the detail of operation.

It is stated that the system was designed for use in large toll centers, particularly those in which subscribers are served by local panel and crossbar offices. Is it to be assumed from this statement that the equipment does not lend itself to function with existing step-by-step installations? On installations such as those at Philadelphia, Pa., it would appear that a need would arise for interconnection with step-by-step systems in order to allow operators to dial directly to stations in existing step-by-step systems. Have provisions been made for interconnection with existing dial offices, making use of two- and four-wire switching?

Does the gate system of marking calls for answering place waiting incoming calls in line for operator answer in the exact sequence that calls were received? When a group of calls are admitted to a controller for answer, are all subsequent calls in the same or other groups blocked until the calls admitted for answering are disposed of?

It has been indicated that the cordless switchboard is intended primarily to serve for inward and through switching, and to serve until toll switching is on a full mechanical basis. Our experience has shown that key-control equipment for outward positions is very desirable and offers definite operating advantages, we question, therefore, whether there is any reason why the type of equipment described will not be applied for outward toll position equipment on new or re-engineered toll office equipments.

The most common method of impulsing over toll lines, as used quite extensively in the foreign field, provides for a voice frequency modulated with a lower frequency of 50 or 60 cycles. Dial impulses are transmitted over toll lines at a speed of operation limited only by the speed of receiving equipments in tandem and terminating toll offices. Since transmittal of dial codes is governed by the speed at which distant office equipments can function, we question how additional speed is brought about through the use of multifrequencies in the switching of dial codes.

On the Philadelphia installation, the crossbar toll equipment works in conjunction with cord-type switchboards. Is this cord-type equipment a necessary complement to the crossbar equipment?

Have limitations been determined to indicate a minimum number of positions wherein the crossbar toll equipment can be applied economically? Also, to what extent will the cordless key-control position equipment be applied to serve outward operators?

F. L. Kahn (Automatic Electric Company, Chicago, Ill.): The crossbar toll switching system as so ably and concisely described in this paper is a monumental achievement exceeding in ingenuity and complexity even the local crossbar equipment. Some of the novel features such as four-wire switching, a-c key pulsing, and the various types of senders show the keenest analysis of the special problems encountered in a large system of this nature; they also evidence the closest co-operation between many different groups of engineers in the Bell Telephone Laboratories, Inc., that deserves recognition, compliments, and respects, and could not be duplicated in any other type or form of research organization.

This paper and the one on automatic ticketing[1] clearly forecast the shape of things to come in the field of toll and long-distance service in this country. These improvements will result in faster completion of toll calls, increased use of these services, and greater efficiency and productive employment of the toll plant.

One of the interesting new developments first mentioned in this paper is a-c key pulsing. A number of patents have been issued in recent years to members of the Bell Telephone Laboratories, disclosing very ingenious a-c keysets. Would it be possible for the authors to give a more specific description of key-pulsing sets and the method of generating the voice-frequency pulses?

The paper states that the sender receives numbers from the operators at the rate of about one digit per second. Does this refer to pulses from a-c keysets? This would be rather slow for an experienced operator. Are there technical reasons that dictate the speed of operation for a-c key-pulsing sets?

In this system, the a-c key-pulsing method evidently reduces the build-up time and thus increases the productive and paid-for time of the toll circuits. Under the most favorable methods, that is, with crossbar switching in the terminating exchange, how many seconds are saved by this method over other conventional pulsing methods?

At the 1939 AIEE winter convention, a paper was presented by Mr. Gilbert Sorber and Doctor A. B. Smith on a "Remote-Control Toll Board" which describes a step-by-step toll switching system developed by Automatic Electric Company and in service in a number of exchanges of independent telephone companies. One of the features of this system is the use of cordless operating positions, not only for inward service but also for outward and delay positions. We have found operators everywhere enthusiastic about cordless operation, which noticeably reduces the fatigue element. This may not amount to many operators' units when figured with paper and pencil, but the service improvement is definitely noticeable. In a large city such as Philadelphia, Pa., it may be more economical to retain existing cord-type outward toll boards; but we wonder whether this new toll switching system will in other future installations be designed for cordless operation on the associated outward and delay positions.

The large keysets on the inward positions appear to be combination keysets and senders. In our step-by-step toll switching systems we employ ten-button keysets and relay-type senders, and have learned that an inward operator requires at least two keysenders for most efficient operation. Do the keysenders on these inward positions dispose of the stored calls fast enough to permit the operators to work at their highest speed?

In our operation of toll switching systems, occasionally we have found it desirable to permit an operator to select a specific circuit in a toll circuit group. Does the crossbar toll switching system provide this facility?

Is there any limit on the size of trunk group that can be selected by dialing one specific toll-circuit code?

L. G. Abraham, A. J. Busch, and **F. F. Shipley** (Bell Telephone Laboratories, Inc., New York, N. Y.): The keysets used for a-c key pulsing are ten-button sets practically identical with sets used for d-c key pulsing. The signaling currents are generated by an oscillator per frequency common to a large number of positions, and are connected in proper combinations to the springs of the keysets.

The pulsing rate of one digit per second mentioned in the paper referred to dialing operation. The average speed of a-c key pulsing by operators is about one-half second per digit. The capability of the operator is the limiting factor, the mechanical and electrical equipment being capable of much higher speed.

With crossbar switches and a-c key-pulsing receivers in the local terminating exchange, a distant operator will reach the called subscriber's line in about three seconds less time by a-c key pulsing than by the conventional dialing method.

As Mr. Kahn suggests, the traffic savings realized by cordless operation on outward toll boards are negligible. Cordless switchboards are more expensive and in some respects not as well adapted to outward and delayed call work as cord boards. There is no prospect of employing cordless boards for these uses in the large toll offices.

The operating speed at cordless positions is not restricted by provision of one sender per position. Digits are transmitted forward by the position sender into other senders at the rate of about 0.14 second per digit, and overlap operation permits a new call to be connected to the position while digits are being transmitted forward. The keyset is always cleared of the preceding call by the time it is needed for a new call.

A specific circuit in a toll group can be selected only with the assistance of the plant force. This provision has proved adequate in practice.

There is no limit on the size of trunk group that can be selected by dialing one specific toll-circuit code. Standard equipment arrangements are provided for test of a group of 160 trunks, but the size can be increased readily if the need arises.

The controlling factor in arriving at the desired switching accomplishments is the use of senders and markers that provide the flexibility demanded by switching requirements of large toll areas. While other types of switches could be used as part of a sender-marker system, the crossbar switch has a number of important advantages, among which are noble metal contacts, reduction of maintenance effort and ready accommodation of a large number of conductors.

The equipment will function with existing step-by-step installations. Step-by-step offices in the Philadelphia, Pa., area and in distant cities are served by the equipment. It will function, also, with distant office equipment of either the two-wire or four-wire type.

Calls are not answered at the cordless board in the exact order of their arrival, but the effect of the gate is equivalent to sequence answering for practical purposes, since protection against material discrepancy in speed of answer is achieved.

When a controller is attached to a link frame, the gate is closed first against subgroups which have no waiting calls and then in each subgroup as it is served, against individual trunks which have no waiting calls. Assume subgroups 1 and 2 admitted within the gate, and subgroup 1 served first. If, while it is being served, another call arrives in subgroup 2, it will be admitted within the gate and be served when the controller advances to subgroup 2.

Additional speed is brought about by use of multifrequency pulsing because the crossbar equipment is capable of accepting and using it with greater speed than equipment driven by step-by-step pulses, whether they are of a-c or d-c type. The complete information to identify a digit can be transmitted with multifrequency pulsing in the time required for a single pulse of the one to ten pulses used with step-by-step pulsing.

The outward and call-order switchboards used in conjunction with the toll crossbar equipment are not of necessity cord-type boards, but are so for reasons of economy.

The lower limit of office size to which the toll crossbar equipment can be applied economically has not been determined. Factors other than size have an important bearing on the question. Examples are the ratio of dial to manual operation, type of local equipment, ratio of through to terminal traffic, and existing plant conditions.

Reference

1. Automatic Ticketing of Telephone Calls, O. A. Friend. AIEE Transactions, volume 63, 1944, March section, pages 81–8.

Electronically Controlled Dry-Disk Rectifier

Discussion and authors' closure of paper 44-8 by Allen Rosenstein and H. N. Barnett, presented at the AIEE winter technical meeting, New York, N. Y., January 24–28, 1944, and published in AIEE TRANSACTIONS, 1944, January section, pages 21–3.

P. T. Chin, O. W. Livingston, and **E. E. Moyer** (General Electric Company, Schenectady, N. Y.): Of the few means available by which automatically to control the d-c output of a non-grid-controlled rectifier the saturable reactor seems to be about the most satisfactory, even though, as in the case of grid-controlled rectifiers, the control of the direct voltage output is achieved at the expense of lagging power factor on the a-c side.

In 1933 when we had available a double-anode rectifier tube suitable for 25 amperes direct current, but did not have a grid-controlled counterpart, a saturable reactor was used to control the d-c output of a rectifier used for constant-potential battery charging. This equipment used a more elaborate control circuit than the one described in this paper because it was both voltage controlled and current-limit controlled in automatic sequence. Current-limit control was obtained by much the same method described in the final paragraph of this paper. The equipment is yet in daily use, but is superseded by the present line of Phanocharger battery chargers which are likewise controlled by the saturable-reactor method in order to utilize less expensive non-grid-controlled rectifier tubes.

As a matter of fact, the present line of Reactrol controls for resistance-heating control use the saturable reactor to control loads of 300 kva, and in these equipments the ratio of d-c control power to controlled kilovolt-amperes is as high as 175:1 to 250:1. Above this kilovolt-ampere level the ignitron tubes in inverse-parallel arrangement are to be preferred on the basis of economics.

There must be a drafting error in the circuit of Figure 3, most probably in that point *E* should be connected to *H* and point *F* to the midpoint of the secondary of *T2S*. Otherwise, there does not seem to be an equilibrium condition due to a restoring force. That is, when the direct voltage increases above a preset level the thyratrons are turned more on, which increases the saturation of the saturable reactor, which increases the direct output voltage from the rectifier instead of decreasing the direct voltage slightly as would be required for stable operation.

The stated gain of 20 to 30 decibels, meaning an amplification of 100 to 1,000, is a lot of gain from a single tube; perhaps this gain is meant to include the inherent amplification of the thyratrons and the saturable reactor.

The behavior of the type of filter circuit described by the authors is such that it completely eliminates the ripple of the frequency to which it is tuned, 360 cycles in this instance. But, the ripple voltage of a six-phase rectifier is not a pure 360-cycle sine wave; instead, it contains higher-frequency components whose magnitude may be increased by the wave distorting action of the saturable reactor, much as in the case of the grid-controlled rectifier. By keeping the degree of saturation low, as may be indicated by the 30:1 ratio of d-c control power to rectifier output, the effect of the saturable reactor may not be very pronounced, but the ripple components of the rectified voltage wave are still present. We usually find it advisable to terminate a resonant filter by some form of untuned filter so as to remove the more objectionable higher-frequency components without sacrificing speed of signal response. In the case of controlled battery chargers the battery itself acts as a huge capacitive filter and thereby simplifies the filtering problem considerably.

Our practice is to use a midtapped supply and bridge arrangement for obtaining a 90-degree-lagging a-c component of grid voltage for phase control of the thyratron tubes from a single-phase a-c source. It is more readily adjusted than the circuit of Figure 5 and is not so sensitive as to magnetizing reactance of the grid transformer *T2P* which is difficult to maintain uniform in a given lot of transformers. However, whether this 90-degree displaced voltage varies a few degrees one way or another is not too important; of far more importance is the wave form of this a-c component of grid voltage.

The use of a glow tube with a holding anode is interesting. Are these tubes commercially available?

J. E. Yarmack (International Standard Electric Corporation, New York, N. Y.): The output-voltage regulation of metallic rectifiers has always been an intriguing problem and many engineers have offered one or another type of control: saturable reactors, standard voltage references, mechanical and electronic devices, or their combination.

It seems proper to emphasize the fact that there are several types of regulation which have been considered. Static voltage regulation is most frequently discussed. It represents the relation of the rectifier output voltage to the output current and is plotted from D'Arsonval-meter readings of the current and voltage for a series of steady-state conditions from no load to full load. There is another type of regulation where the dynamic characteristics represent the relation between the rectifier output voltage and current but regulation is obtained while keying or interrupting the load or a combination of loads at varying rates by measuring the instantaneous voltage and current values.

The use of gaseous tubes as voltage standards with which the output voltage of the rectifier is compared is found to be satisfactory. This offers an even more promising method of controlling the output voltage. Its use within the international system has been most gratifying.

Without criticizing the authors for the style and terminology used in the paper, it is desirable to point out that a subcommittee of the AIEE has been appointed and has been at work for some time with a view toward standardization of nomenclature and graphical symbols concerning metallic rectifiers. If and when such standards are agreed upon and are adopted, the names such as "dry-disk rectifiers" or "selenium-oxide rectifiers," we hope, will disappear and the common understanding of the terms will be a benefit to all engineers and users of metallic rectifiers.

W. E. Phillips (Leeds and Northrup Company, Philadelphia, Pa.): I would like to first congratulate the authors, both as regards their material and presentation.

The filter circuit described is especially interesting since we have been using this circuit, with the addition of two slide wires as shown in Figure 1 of this discussion, for measuring commercial frequencies for a considerable time and also for filtering out certain undesired frequencies in circuits.

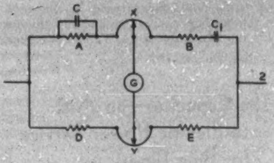

Figure 1

An examination of the vector quantities involved in a bridge of this type will, I believe, be of interest.

Figure 1 shows the bridge circuit as employed for this purpose.

Figure 2 is the vector diagram for the bridge when balanced at its designed frequency. It should be noted that the left-hand upper arm has been converted to the equivalent series circuit in order to show the operation more easily vectorially. The two vector triangles are exactly similar so that they both have the same phase angle and consequently the point where they join will lie on the applied voltage vector E_U. It should also be noted that the voltage across the lower arm E_L is the same voltage applied to the upper arm and is displaced in the figure merely for purposes of explanation. If we then connect a galvanometer between point *Y* which is the midpoint of E_L and point *X* (which when the designed frequency is applied to the bridge) will be at the midpoint of E_U, there will be no voltage of the design frequency between points *X* and *Y*.

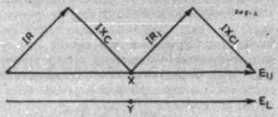

Figure 2

Figure 3 is the vector diagram when a higher frequency is applied to the bridge. This results in a voltage E_g across the center of the bridge since for small changes in frequency the a-c resistance of the left arm changes, the reactance remaining substantially constant. In the right arm, the reactance changes while the resistance remains constant. Thus as a frequency measuring device the bridge can be balanced by inserting resistance in the left arm and cutting out resistance in the right arm.

The previous explanation will serve to show that if the bridge were designed for some harmonic of a fundamental frequency, that the harmonic would be entirely attenuated while the fundamental would come through with only a small amount of attenuation. By properly proportioning the vector triangles so that they are distinctly different in magnitude, but otherwise similar and similarly changing the ratio of the resistances in the lower arm, the attenuation of the fundamental may be materially reduced as explained by the authors.

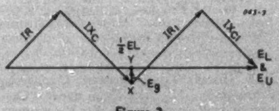

Figure 3

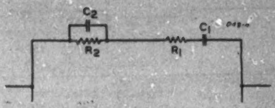

Figure 4

$$f=\frac{1}{2\pi\sqrt{R_1R_2C_1C_2}}$$

Certain fundamental relations exist in the bridge and one of importance in regard to the upper arm as shown in Figure 4 is that for equal phase angles in the two halves of the upper arm

$$f=\frac{1}{2\pi\sqrt{R_1R_2C_1C_2}}$$

Allen Rosenstein: The conformation of our conclusion regarding the economy of saturable reactors by Chin, Livingston, and Moyer is gratifying. Of particular interest are the high ratios of d-c control power to control kilovolt-amperes which have been obtained.

The circuit shown in Figure 3 of the paper is erroneous in that the connections to points *E* and *F* from *H* and the midpoint of *T2S* are reversed.

Gains of 20 to 30 decibels are obtained from a single tube by use of a carefully selected tube type and a critically designed circuit. The changes in internal resistance of the tube cause changes in the voltage drop across the tube. These voltage changes are such as to tend to limit the amplification obtained. By using a beam-power or pentode tube operating with a comparatively low screen-grid voltage this effect is minimized. The variations in tube plate current will cause changes in the screen-grid current. If the potential for the screen grid were obtained from a resistance divider, these changes in the screen-grid current would vary this potential and tend to limit the tube amplification. The effect is eliminated and further stability obtained by using the constant voltage across the gaseous regulator tube to supply the screen-grid potential. It might be pointed out that it is not necessary to obtain a very large voltage variation from the amplifier to operate the phase shifting network. Therefore it is possible to set the amplifier to perform at its point of highest efficiency. Within the limits set by the current demand of the phase-shifting network, additional amplification is obtained by increasing the resistance in series with the tube. Further amplification may be secured by increasing the voltage across the tube and its series resistance. The over-all result is naturally dependent upon the characteristic of the vacuum tube used. We have found that a

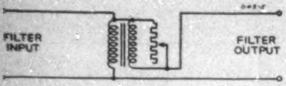

Figure 5

tube having an extremely high transconductance coupled with a sharp cutoff gives excellent results.

The output of a six-phase rectifier will not only have a fundamental ripple voltage of 360 cycles, but will contain higher-frequency components whose magnitudes decrease with increased frequency.

The Wein-bridge filter that has been described, will completely eliminate the fundamental ripple frequency and will offer decreasing attenuation for increasingly higher harmonies. By suitable circuiting, it is possible to also apply a Wein-bridge filter to the output of the d-c amplifier. This filter can be tuned to a higher frequency than the amplifier input filter which is tuned to the ripple fundamental. The use of the two Wein-bridge filters, coupled with a "high pass" filter on the rectifier output, offers a very satisfactory arrangement.

In the search for a filter with low time constant, low d-c attentuation, and high a-c attenuation, another filter was developed that proved quite practical. (Figure 5 of this discussion.) This filter consists solely of a transformer and a resistor. The output winding of the transformer is connected and adjusted to equal and oppose any alternating voltage that might be impressed upon the input winding. The resistor provides stability and a means of adjustment. For satisfactory filtering, care must be taken in the design of the transformer. In particular, allowance must be made for the fact that direct current flowing through the input winding tends to saturate the transformer. Excellent a-c attenuation, with no d-c attenuation, and very little time lag, can be obtained from the transformer filter.

The RCA type *OA4G* glow tube has proved of value as a source of constant potential. This tube has a second anode which is used to fire the tube.

As pointed out by Mr. Yarmack, there are actually two kinds of regulation to be considered—the more commonly talked of static regulation and the dynamic regulation that is obtained under changing load. Usually the most important consideration of the dynamic regulation is that of the recovery time. This is the time required for the voltage to return to its static value when there is a sudden change in load. A study of the circuit of the electronically controlled selenium rectifier will show that the recovery time of the unit will be a function of the build-up or decline of voltage through the filter and the rise or decay of saturating current through the saturating reactors. Existing units show recovery times of between one-fourth and one-half second. By careful design, this time could probably be reduced further. Extremely fast recovery could be obtained through the substitution of ignitrons in inverse-parallel for the saturating reactors.

The forthcoming standardization of metallic rectifier nomenclature promised by Mr. Yarmack is looked forward to by all who have had to interpret the growing list of terms and symbols used in this field.

The authors wish to thank Mr. Phillips for his able discussion of the Wein-bridge filter.

The Capacitor—an Aid to Electric-Power Service for Pipe Lines

Discussion and author's closure of paper 44-40 by M. A. Hyde and R. E. Marbury, presented at the AIEE winter technical meeting, New York, N. Y., January 24–28, 1944, and published in AIEE TRANSACTIONS, 1944, pages 387–95.

W. G. Taylor (nonmember; General Electric Company, Schenectady, N. Y.): In the portion of their paper concerning the War Emergency Pipelines, Inc. Hyde and Marbury show that, when the main pump motors are started on the 80 per cent tap of the autotransformer, the demand, with two units in operation and the third one being started, is about 25 per cent less than when starting at full voltage. This is evidently based on the assumption that full-rated voltage of 2,300 is maintained on the pumping station bus. In "Electrical Equipment for the War Emergency Pipe-Line Projects," by Thomas, Taylor, and Wolfe,[1] it is stated that, under the most unfavorable power conditions, only about ten per cent reduction in starting kilovolt-amperes of the motor can be expected from reduced-voltage starting. With two motors already running, the reduction in station demand would be even less in percentage.

Although these statements are not inconsistent, and this is indicated by the explanation in the aforementioned paper, it might be well to emphasize that voltage drop on a soft power system may so reduce the motor-starting torque that the margin above the pump torque would be insufficient to accelerate the motor to full speed. This condition is accentuated by the use of the reduced-voltage starting method. In such a case it would be necessary to adjust the reduced-voltage starter to connect the motor directly to the line at some reduced speed, usually below 75 per cent of full speed, when the accelerating current and kilovolt-amperes are still nearly as high as at the instant of starting. Thus, when there is considerable line and transformer voltage drop under motor-starting conditions, the effect of starting the motor across the line is equivalent to the use of a reduced-voltage starter, and the latter may be of little further advantage. On the other hand, a stiff power system may not require reduced-voltage starting. Therefore, in either case, a reduced-voltage starter may be superfluous.

REFERENCE

1. ELECTRIC EQUIPMENT FOR THE WAR EMERGENCY PIPE-LINE PROJECTS, E. E. Thomas, W. G. Taylor, Oscar Wolfe. AIEE TRANSACTIONS, volume 63, 1944, pages 396–405.

E. E. Thomas (General Electric Company, Schenectady, N. Y.): Data under "Capacitor Applications on the War Emergency Lines" evidently were based on motor name-plate voltage of 2,300 volts. These data should be correlated with those in the Thomas, Taylor, and Wolfe paper in reference 1 of W. G. Taylor's discussion. The information in Table II of that paper was based on expected operating conditions at pump stations 7, 8, and 9. It is believed that such data in both papers will check when all factors are taken into account.

Capacitors were added at pump stations 7, 8, 9, and 20 by the War Emergency Pipelines, Inc., as the quickest means to obtain a satisfactory voltage level at these locations. Because of the short write-off period, investment for these capacitors is not self-liquidating on a power-factor-clause basis.

No doubt the capacitors for continuous operation on the Plantation Pipe Line represent an attractive investment, since it is understood that the power contract power-factor clauses are such that these capacitors will be paid for in about 2$^{1}/_{2}$ years.

If series capacitors were investigated for intermittent operation to provide motor-starting service at Fluker and Roxboro Pump Stations, a statement of the result of such investigations should be interesting and useful. The use of a special autotransformer and capacitor combination at the Roxboro Station for obtaining suitable motor-starting voltage and, also, to prevent excessive momentary overvoltages appearing at nearby power customer locations is an interesting procedure. It would be helpful to know the magnitude of harmonic currents in this local capacitor circuit.

W. H. Stueve (War Emergency Pipelines, Inc., Cincinnati, Ohio): The viewpoints presented in this discussion are offered as coming from one knowing all the intimate details of the circumstances surrounding the installation of capacitors on the 24-inch crude line and the 20-inch products line installed from Texas to Eastern tidewater.

I am not familiar with the capacitor installations in the Plantation Pipe Line pump stations, and for this reason I will confine my remarks to the installations in the War Emergency Pipeline stations. I believe, however, from Hyde and Marbury's paper, that the capacitors were used in the Plantation stations for the same reason we were forced to install such equipment for the "Big Inch" stations, namely, the lack of a "stiff" power supply in the power systems from which energy was contracted for the station operation.

For this reason it strikes me that the title of this paper may be somewhat misleading, in that the capacitors for the "Big Inch" project were installed only to:

1. Increase the voltage to enable the motors to start satisfactorily.
2. Maintain nearly 2,300 volts at the motor terminals to prevent overheating during certain seasons of the year when the power-supply voltage was very low for continuous long periods of time.

This situation is not inherent with pipeline pumping stations, since it would apply to most any sort of industrial load studied.

Let me say that capacitors would not have been installed in the War Emergency Pipeline stations except for the fact that the power-supply system was so weak and unstable for stations 7, 8, and 9 (not through any fault of the power suppliers) with primary voltages falling as low as 95 kv at the origin of our lateral feeders, during certain periods of the year, sustained for long periods of time, that it was impossible to secure sufficient secondary starting voltage for those stations 7 and 8 where we already had purchased full-voltage starting equipment.

If our problem had been one of starting only, no doubt much cheaper series capacitors could have been used. The long durations of low-voltage conditions precluded this approach to prevent overheating of the equipment.

To review the conclusions reached by us to install capacitors, which would apply only to this one project and not to pipe-line stations in general, when the 24-inch line first was considered in August 1942, stations 7 and 8 each contained 4,500 horsepower and the War Production Board insisted on our using steel conductor in the 62 miles of power line (110 kv) required. The power supplier indicated the lowest supply voltage would be 105 kv with eight per cent regulation in the steel line plus five per cent in the 3,750-kva transformers. The fourth five per cent transformer tap would give sufficient starting voltage, and no capacitors even were contemplated.

Later, in January 1943, the 20-inch products line was authorized, requiring 3,750 horsepower additional at stations 7, 8, and 9, all power to originate at Walnut Ridge, the point of power take-off of the steel-conductor line. It was recognized immediately that the steel conductor could not be used, and we were granted permission from WPB rather reluctantly to use Copperweld and aluminum in the new 120 miles of 110-kv line required.

We insisted on and obtained this conductor of sufficient capacity to start all motors at all the stations 7, 8, and 9, basing our computations on the supply voltage at Newport and/or Walnut Ridge being the same as given earlier in August 1942 at 105 kv minimum.

When the power supplier started worrying about his ability to maintain a stabilized voltage at all seasons of the year, after studying our new load of 8,250 horsepower in each station 7 and 8 and 5,250 horsepower at station 9, the project was then placed on the calculating board. Early in March 1943 we were advised that voltages might approach 90 kv at Walnut Ridge, and the whole system would become unstable and probably collapse.

The power supplier strongly urged the installation of about 12,000 kva of capacitance in our system and advised that they expected to install 5,000 capacitive kilovolt-amperes themselves. Our calculations indicated at this time that the only solution to obtain relief was to install about 75 per cent of the capacitors recommended by the power supplier as a minimum.

The application and capacity required in each station, however, were reached after giving consideration to the possibility of one or the other pump houses being shut down at the time of starting, and for this reason each motor was equipped with its individual capacitors and resulted in following the power supplier's recommendations in entirety.

In no sense can it be construed that these capacitors we installed will pay out from a standpoint of economics by lowering the demand charges. In the case of stations 7, 8, and 9, a total of 17,400 kw, the billing demand is reduced to about 15,000 kw or a saving of about $30,000 per year by the power-factor adjustment clause in the rate applying, which will require about five years to return the investment. In the case of the capacitors installed at station 20 in Pennsylvania, this was made for the same reason, in that an unstable supply voltage prevailed at the origin of the ten-mile 25-kv feeder line. The power supplier recommended about 2,000 capacitive kilovolt-amperes in each pump house or a total of 4,000 capacitive kilovolt-amperes.

After having studied these calculations, however, our conclusions were that none would be required, but to satisfy them we discounted their recommendations 50 per cent and installed only 2,160 capacitive kilovolt-amperes at station 20. It now appears that our figures were too high, and that the capacitors may not be necessary for starting or running the 8,250 horsepower installed. The power rate at station 20 does not contain any power-factor benefits, so this investment never will be repaid.

Thus, you can see all the installations of capacitors were made only under pressure, and it cannot be said that they "aid" the power service, because if "stiff" power systems were available, these installations never would have been made.

The installations are not sound economically, and from a pipe-line operating standpoint are "just so much more electrical gadgets to maintain." I think I am safe in saying that the pipeliner would prefer to do without these installations.

The paper includes interesting detail of the starting kilovolt-amperes of the motors, and how the capacitor effects changes in voltage to permit better and quicker starting when the pumps are started against a closed head gate. The discussion of the problems encountered on the Plantation Pipe Line are very informative and should be helpful to the industry as a whole to show the successful use of capacitors to bolster a weak power-supply voltage.

I believe, however, that the authors' approach to the "Big Inch" installations should have dealt with the problem as an individual proposition for a specific case and not with generalities, thereby conveying to the casual reader that all pump stations should have capacitors installed to "aid their power service." On the War Emergency lines only four pump-station locations out of a total of 29 station locations have capacitors installed.

M. A. Hyde: In reply to Mr. Taylor and Mr. Thomas, the values given in our paper for motor and station current and kilovolt-ampere demands, both for running and for starting, unless otherwise specifically stated, are based on 2,300 volts. These values are given at rated motor voltage as a uniform basis of comparison, and it will be understood, of course, that the current and kilovolt-ampere values actually realized at the several stations will vary according to the actual voltage at each station, which depends on stiffness of the power supply. In this respect, each station constitutes an individual problem.

With regard to the question as to why more extensive use was not made of full voltage starting in these pipe-line pumping stations, I would say that the decision as to whether to use full-voltage or reduced-voltage starting followed in general the preferences of the various power companies serving the stations. For a particular pumping station, the choice between across-line starting and reduced-voltage starting is determined by the ability of the power system to handle the starting demand with voltage service acceptable not only to the pipe line but also to other power users on the system. In some cases, where full-voltage starting would have been satisfactory from the standpoint of the pumping station itself, reduced-voltage starters actually were installed in order to meet the more exacting service requirements of other nearby power customers on the same circuit.

At both Fluker and Roxboro stations, consideration was given to the application of series capacitors. In applying series capacitors on distribution circuits, precautions must be taken against the occurrence of resonant conditions; more specifically, the possibility that during motor starting the rotor may acquire some subnormal speed and continue to rotate at this speed because of circuit resonance at some frequency below 60 cycles, at which the motor rotor acts as a stable generator. It is good practice to make a special study of cases where the motor to be started represents more than ten per cent of the minimum load supplied through the series capacitor. In the Fluker and Roxboro stations this ratio would be far exceeded. In the case of Roxboro station, calculations on the likelihood of subsynchronous resonance were supplemented by calculating board studies on this phenomenon as applied to the conditions surrounding this particular application, and the results of these studies indicated that the motor would operate at a subnormal speed. Obviation of this starting difficulty by using a resitor in combination with the series capacitor involved the cost of the resistor, which, with the necessary control, did not make an attractive over-all picture for the series capacitor. This was particularly true in view of the saving in shunt capacitors effected by their application on an intermittent basis for the starting period only.

In the case of the Roxboro station installation it is quite logical that a question should arise as to the occurrence of harmonics. The extent of harmonics can be judged from Figure 10 of the paper which shows laboratory test oscillograms taken at applied voltages ranging from about 1,200 to 3,100 volts. As stated in the paper, the supply circuit contained resistance and reactance approximately matching the values for the Roxboro station supply feeder, and there is no question but that the harmonic currents were reduced by the supply-circuit resistance; in fact we demonstrated this during the course of the test. It is our opinion that this feature should be given special consideration where the scheme is applied on supply circuits of low resistance; however it may be kept in mind that the scheme itself is of value only on weak supply systems, where the feeder resistance is likely to be appreciable.

Mr. Stueve's discussion brings out very clearly the point that capacitor installations on the War Emergency pipe lines were made only at a limited number of stations where the power service was considered too weak to handle, otherwise, the incremental pumping-station load. Indeed, with the high-speed motors characteristic of these installations, operating at full-load power factors of 0.91 to 0.92, there is no need for capacitors under ordinary conditions of power service. The authors have no wish to present capacitors as a desirable adjunct to every pipe-line pumping station. This is evidenced by the enumeration of seven stations as employing capacitors out of a total of 57 stations shown in Table I of the paper for the two War Emergency lines.

In each case where capacitors were installed, the objective was to attain power service from a power supply considered inadequate to serve the load without such corrective effect. Capacitors were certainly a definite aid to power service to these stations. In the case of station sites 7, 8, and 9, it would seem that, with the accomplishment of power service at minimum expenditure of critical materials together with return of the capacitor investment in five years, as mentioned by Mr. Stueve, capacitor installations at these stations perform an essential service at reasonable cost. A similar situation holds in the case of the Plantation pipe line where the average pay-out time for the continuous capacitors—based on operating experience since their installation—will be about $2^1/_2$ years.

At station site 20 of the War Emergency Pipelines, Inc., where no pay out is realized by reduction in power billings, the utility of capacitors depends entirely on their necessity as a reinforcement to the power system. The same is true of the installation at Plantation's Fluker station.

One more point—because hydraulic considerations may, and frequently do, dictate the location of some stations at sites particularly unfavorable for power service, the value of these capacitor installations must be considered in relation to the operation of the line as a whole, aside from the question of pay out at each individual station.

The Vibration of Electric Contacts

Discussion and authors' closure of paper 44-69 by Mark N. Russell and Saul Keilien, presented at the AIEE winter technical meeting, New York, N. Y., January 24–28, 1944, and published in AIEE TRANSACTIONS, 1944, April section, pages 153–5.

I. R. Schumaker (nonmember; General Electric Company, Schenectady, N. Y.): The authors have stated that "considerable attention has been given to the study of current-carrying ability and circuit-interruption ability of contacts, while the 'making' of contacts has been practically neglected." It is not to be assumed that little is being done on this score just because little or nothing has been published about it. Several years ago some 12 contact-testing machines were produced for member companies of the American Society for Testing Materials subcommittee on contacts to the specifications of that subcommittee and for the sole purpose of testing contact-making ability. This would indicate that considerable work had been done before that time and that an even more concerted effort is being made now. In addition, the demands of our Air Forces in this war for contactors and relays that will do tremendous jobs in small-sized packages has given the manufacturers an even greater realization of the problems involved in the "making" of an electrical circuit. However, the data that have been taken up to this point are so inconsistent that it is difficult to arrive at any conclusions. To be more specific, seven of the ASTM machines operating under the same conditions of load, speed, and pressure and using tips made by the same manufacturer in the same lot, showed a variation in total number of welds after 250,000 operations of 2 for one machine to 500 for another, with the others well distributed between these limits!

When making and breaking a d-c circuit, there are at least three factors that affect the life of the contacts. Two of these, as the authors have indicated, are bounce on "make" and arcing on "break." The third is material transfer which is, among other things, a function of contact material. This could produce a condition such as is shown on both of the contacts in Figure 2*a*. Since the authors have not identified the contacts shown, it is not clear to me whether they made mutual contact or if there was another member not shown. Likewise, there is no description of the action of the contacts so it is difficult for one to draw the same conclusions as the authors from the data presented.

The authors present no data to back up their statement that "similar destructive results occur in switches controlling motors, since the starting current of motors is several times their running value." It has been our experience that a lamp load presents a far greater problem in circuit making than a motor load due to the extremely rapid rise of current in the case of the lamp. The motor, being inductive, allows a far slower rate of current rise and bounce frequency in general being quite high, circuit interruption occurs at relatively small values of current resulting in only slight burning of contacts.

Data we have taken indicate that there are more factors affecting bounce than the authors have included. The outstanding factors are preloading of contacts, speed of closure, weight of moving parts, resiliency of contact material and of contact structure, amount of current to be made, and voltage of circuit. Russell and Charbonneau in their paper "Aircraft Contactors" [AIEE TRANSACTIONS, volume 62, 1943, September section, pages 563–6] show that preloading can be either too little or too great. The authors of the paper under discussion have indicated that preloading must be greater than a certain amount, but do not indicate how far this may be carried.

Mark N. Russell and **Saul Keilien:** In the discussion, mention was made of the construction of 12 testing machines built for the member companies of ASTM of committee *B*4, section *B*, subcommittee *X*. The authors were thoroughly conversant with the existence and the operation of these machines, but, as it is correctly remarked, on consistent data have been obtained. This fact was substantiated even at the last ASTM meeting held on February 21, 1944. Consequently, no specific recommendations for suppression of contact vibration had been offered.

Inasmuch as our paper dealt with vibration of contacts, we considered the transfer of material on contact points was outside the scope of this paper. Due to the limited space, it was necessary to omit some of the details of the contact action, and the discusser may be interested to know that the contacts in question were mutually preloaded blades of a snap switch straddling the stationary contacts, thus completing the circuit.

While it is true that motor circuits offer a problem of lesser severity than that of the tungsten filament lamp, there is an inrush greater than the normal current, which would be quite a factor if the contact vibration is severe.

It is questioned by the authors whether or not such factors as the current value and voltage of the circuit affect the contact vibration, as stated in the last paragraph of the discussion. The attention of the discusser is called to those paragraphs in the paper in which the velocity of contacts and weight of moving system are considered. No mention is made of the upper limit of preloading, for it is apparent that a point may be reached where excessive preloading would cause failure of the moving blade to engage the stationary contact because of insufficient energy to overcome the pressure of the contacts.

Cultural Training of the Engineer

Discussion and author's closure of paper 44-3 by A. Boyajian, presented at the AIEE winter technical meeting, New York, N. Y., January 24–28, 1944, and published in AIEE TRANSACTIONS, 1944, January section, pages 6–9.

S. T. Maunder (General Electric Company, Pittsfield, Mass.): Mr. Boyajian's paper is a refreshing presentation of a subject that is popular with every engineering student and graduate—rearranging the curriculum. The paper makes sense out of the cultural versus scientific controversy, by introducing *applied* psychology and *applied* English for the engineering student.

The present English course easily can be changed to include many of the points that the author brings up. An engineer is usually poor in English composition, but the schools have tried to teach him from the English department's viewpoint—how to write well. Reverse the viewpoint, forget the English department's usual procedure, and start a course in applied English; that is, how to write so that desired action will take place.

Suppose the professor started the class one morning with a story: "A development engineer often has to ask for an appropriation to continue his present work, or to start on a new study. How can this be worded most effectively? One engineer whom I knew had a formula which enabled him to have nearly all his applications approved. Eventually, he had so much work to do that men were assigned to help him, and then he became the head of an important development section; all because he knew how to ask for and obtain appropriations while others were not so fortunate. Here is his formula."

If that were told to the students they would furiously copy the golden formula. Then, when told to try it out for the next class on some idea they'd like to develop, they would do a good job.

This is not sugar-coating English to make it palatable; it is English composition that is so practical that the student will be convinced that he must learn it. To this add oral presentations and lectures on applied psychology. But now it is no longer English. It is business management, applied psychology, or executive technique. Change the name to express the new objective.

Thus, out of the former unprofitable and disliked engineering English, a new course will grow which will teach students those oral and written arts of influencing human action. A student of such a course, by using this knowledge, can become a superior engineer.

David C. Prince (General Electric Company, Schenectady, N. Y.): The title of this paper may be somewhat misleading. The author is actually asking what it is that an engineer needs in education in addition to the bare technical necessities of his calling.

The existence of such additional educational needs has been generally recognized, and various educators have recommended what have been loosely described as cultural courses—varying all the way from perhaps some further emphasis on English to the feeling that an academic degree should be a prerequisite to an engineering training, just as it is at present to training in medicine and law.

The war has thrown this subject into a much stronger and more searching light. The very survival of the country has depended upon the training of our youth, either before or after being inducted into the armed services or into essential wartime industry.

The war effort has leaned heavily on men who have had engineering training. This was more or less to have been expected because of the importance of material in mechanized war. But the discovery that is startling to some people has been the need for training in leadership.

Are not the three needs which Mr. Boyajian lists specifically as "student's needs" the prime essentials of the engineering graduate to be trained for a position of leadership? First, he must determine the requirements of each new situation. Then, he is to be a leader, these requirements must be made clear to other people. To that end, first he must understand those requirements himself and then the ideas must be communicated to the others, either by speech or writing, broadly interpreted.

Most of our cultural subjects have ministered to the broader perspective required of leaders, although I agree with the author that far too little thought has been given to seeing that the objective is really reached by these courses as they have in times past been selected and taught.

A. Boyajian: It is very gratifying that the major ideas expressed in the paper are approved of by Mr. Prince and Mr. Maunder and a number of others who have written personally to the author.

The last sentence of Mr. Prince should be kept in mind by the educators. It is not enough to replace one course with another; we must see to it that the chosen course is taught the proper way to accomplish the objective.

The discussion outside of the session has been a great deal more than at the session, most of it with enthusiastic agreement, some with emphasis on the importance of the precollege training and parental attention, about which, of course, there can be no doubt. In the cases of both the lower schools and the colleges, the first step is to crystallize the objectives, and then develop the methods of training to accomplish those objectives. High schools try to prepare their students for the colleges to which each student intends to go. (At least, it is so in Pittsfield, Mass.) So catalogs are consulted to make sure that the requirements are satisfied in each case. But, at present, what the high schools know of these requirements is the courses and a scholastic-aptitude test, and sometimes additionally a scholastic-achievement test. When the colleges begin to give special attention to the psychology of the freshman, and some of the competent faculty members occasionally lecture on the matter to high-school faculties, we may begin to see improvement in the cultural training of the high-school graduate, too, whether for college or for life.

Basic Considerations in Selection of Electric Systems for Large Aircraft

Discussion on authors' closure of paper 44-36 by W. K. Boice and L. G. Levoy, Jr., presented at the AIEE winter technical meeting, New York, N. Y., January 24–28, 1944, and published in AIEE TRANSACTIONS, 1944, June section, pages 279–87.

Jerome J. Taylor (The Detroit Edison Company, Detroit, Mich.): The authors give a table showing the influence of system voltage on the weight of the power-distribution-system *copper* in large aircraft. They also state that the present 27-volt d-c electric system has done an excellent job to date, but becomes inadequate from the standpoint of weight for application to planes now being designed. These planes are larger, and presumably require greater amounts of electric power, than the *B*17 and the *B*24.

In this situation it would seem logical to use aluminum conductor instead of copper for electrical wiring of aircraft. The weight of aluminum is less than half that of copper of equal conductivity, and, using the authors' figures, a 27-volt transmission system employing aluminum conductor therefore would be approximately equal in weight to a 120-volt d-c two-wire system using copper. By this means the advantages of the 27-volt system could be extended to larger planes. Alternately, aluminum could be used in the higher-voltage systems, with consequent weight reductions. There are other possible advantages, such as behavior under gunfire and burning-clear characteristics. A major disadvantage has been the relative difficulty of making good connections, and in a 27-volt system low contact resistance is vital. There have been progressive improvements in this art, however, and methods of making reliable joints are now available. Will the authors state whether there are other factors which militate against the use of aluminum conductors?

A. H. Powell (nonmember; General Electric Company, Philadelphia, Pa.): The problems involved in the selection and design of electric systems for large aircraft are similar to those in small industrial and power systems. Yet aircraft circuits involve additional complications that prevent the entire use of time-tested methods and designs. As the authors have pointed out in their well-written comprehensive review of past, present, and probably future aircraft systems, alternating current rather than low-voltage direct current is selected for large planes involving long cable runs and large loads. Furthermore, the type of machines used and, in general, the protective system employed follow conventional power practices.

However, aircraft equipment differs from standard commercial material in that it must operate under the most severe conditions of temperature, humidity, dust, corrosion, and vibration. At the same time, it must be of a minimum size and weight, yet provide maximum reliability. Such requirements have necessitated a change in design practices and thoughts, and the development of new materials and methods of manufacture which may radically affect the future equipment used on power and industrial systems.

This paper shows that 208-volt three-phase 400-cycle circuits are the choice for large long-range planes of the future. Devices now available for these circuits operate at temperatures and speeds greatly in excess of similar conventional 60-cycle units, with a corresponding decrease in weight and increase in unit output that is quite astonishing. The designer of commercial equipment should realize that the ultimate in commercial design by no means has been reached. We can expect to see radical changes in electrical equipment to take advantage of the knowledge gained by development of aircraft devices.

The subject of protection of the a-c circuits in aircraft was only mentioned in the paper in passing. Therefore, it might be well to point out that protection of a-c aircraft circuits indicates a new trend that might well be followed for industrial or power systems in the future. Individual equipment uses built-in thermal protection designed to obtain maximum output under all conditions of temperature and load. Fuses are used only for fault protection, not overload protection, and circuit breakers at the main feeder bus are the final link in the protective chain. A fuse with a high-temperature melting link, and consequently "fast" characteristics, is suited ideally to such a scheme, as it is selected to carry the maximum load current and the overload current for the length of time allowed by the thermal protector, yet will interrupt the circuit with minimum clearing time when a fault develops. Such protection for each branch circuit provides the minimum disturbance during a fault, but allows the connected load to operate at its peak capacity—an ideal arrangement, especially for aircraft circuits.

It is true that d-c circuits probably always will be used in aircraft. However, it is also true, as pointed out in this paper, that a-c circuits will play a big part in the operation of large aircraft, and apparently are the logical answer to the question, "What type of power system will be selected for the future planes?"

C. Concordia (General Electric Company, Schenectady, N. Y.): In connection with the authors' remarks about the severe requirements entailed in the driving of paralleled synchronous a-c generators from the engines through a controlled speed-reducing mechanism, Figure 1 of the discussion may be of interest.

These curves show the rate of change of electrical angle (in radians) between the generator whose driving engine is being accelerated and the other paralleled generators of the electric system.

Curve A is for a drive with no slip. The engine acceleration is assumed as equivalent to 100 per cent speed change in two seconds, which is not the maximum possible. The only control is assumed to be a flat-speed governor which has a speed of response proportional to the deviation of the generator speed from normal, the constant of proportionality being such that one per cent speed deviation gives a rate corresponding to 100 per cent speed change in one second. The generator frequency is 400 cycles per second. Leaving out of consideration freewheeling or any overload relief valve in the driving mechanism, it is seen that, with this simple control, if this engine acceleration is maintained for much more than one tenth of a second, synchronism will be lost. (Such an engine acceleration may persist for much longer than 0.1 second.)

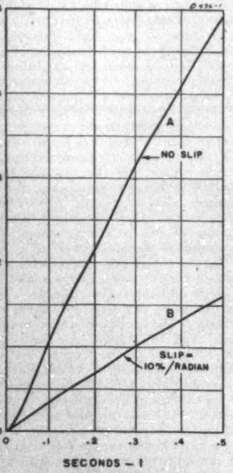

Figure 1. Curves showing the rate of change of electrical angle

Engine acceleration = 50 per cent per second
Governor full stroke in one second for one per cent speed deviation

Curve B shows the results for the same case as curve A except that now the drive of the generator being accelerated has a linear slip-torque characteristic such that ten per cent slip corresponds to one electrical radian angular displacement. No lag in the governing mechanism has been considered in either case, so the calculations are optimistic in this respect. To match the characteristic of curve B without slip in the driving mechanism would require approximately three times the rate of response in the speed governor, that is, full stroke in one second for one third of one per cent speed deviation. The question of whether or not such a speed of response under transient conditions, or even the slower rate of curve A for that matter, can be realized will depend primarily on the stability requirements which must be met by the control system.

These results emphasize the authors' conclusion that the control for such a drive must meet very severe requirements and that some freewheeling or relief-valve mechanism probably will have to be incorporated. A direct (that is, undelayed) response of the control to load as well as to speed would also be of help in this regard, but such a control is difficult to make stable.

It is of some interest to note that the angular rate of departure from synchronism is proportional to frequency, and that a lower frequency tends to relieve the control. Thus there is an interrelation between the driving means and the optimum system frequency.

V. A. Higgs (nonmember; Warwickshire, England): The tremendous amount of development already put into the 24-volt d-c system is stressed in the paper, but I consider its further use is dismissed too easily. It is agreed that for very large powers the heavy currents demanded are a very great drawback. On the other hand, for medium-size aircraft it is thought that considerable improvements can be made over the existing conventional layouts.

SINGLE-ENGINE INSTALLATION

Figure 2 of the discussion shows a simplified diagram of a typical single-generator installation. The accumulator (storage battery) is included in order to relieve the generator of peak overloads, to act as standby in the case of failure of the generator, and to provide power on the ground. In order to obtain power from the accumulator, it is necessary for the system voltage to change from 29 volts, which is necessary to charge the accumulator, to something less than 24 volts. If the discharge current is heavy, the voltage may be reduced as low as 20 volts.

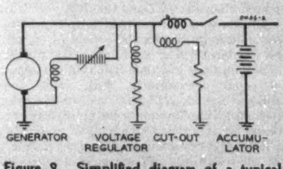

Figure 2. Simplified diagram of a typical single-generator installation

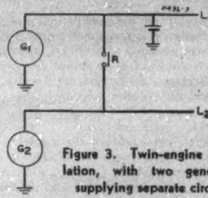

Figure 3. Twin-engine installation, with two generators supplying separate circuits

This variation in line voltage places a severe handicap on the design of motors, dynamotors, inverters, and so forth. In addition, the amount of power available from the accumulator is very limited, unless the size and weight are considerable.

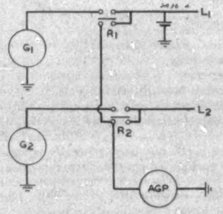

Figure 4. Schematic diagram of a system with an auxiliary generating plant added

By increasing the capacity of the generator it is possible to obtain a far greater output for much less weight, so that under normal flying conditions, the arrangement shown in Figure 2 would be better without an accumulator. Such an arrangement, however, would provide no power when the aircraft is on the ground and the main engine is not running. By extensive development of nonelectrical methods of main-engine starting, the value of the power supply on the ground is greatly reduced, but it may be argued that in certain cases it is an essential feature that aircraft should be self-supporting, so far as electric power is concerned, when on the ground.

An auxiliary generating plant (*AGP*) consisting of a small petrol engine driving a d-c generator can be designed to give a continuous output for less weight than an accumulator giving the same output for only five or six minutes. The introduction of an auxiliary generating plant would introduce problems not present with the accumulator but these problems all have been overcome to a large extent and there are already in existence numerous forms of auxiliary generating plants. Further development of

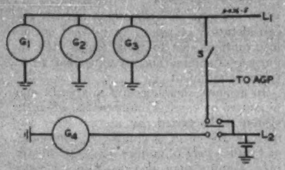

Figure 5. A four-engine layout combining parallel operation and separate circuits

auxiliary-generating plant design will be essential for the utilization of alternating current in aircraft, and this development can also be put to use in d-c power systems.

It is considered not impossible to introduce a small efficient auxiliary generating plant into single-engine aircraft, giving an over-all weight no greater than generator–accumulator combination and giving far better performance.

It should be noted that certain classes of aircraft demand a small power supply for short duration when all other power supplies have failed, and to meet this it will be necessary to carry a very small accumulator less than five ampere-hours capacity, which, since it never plays any part in the provision of main power, can "float" on the bus bar to which it is connected.

TWIN-ENGINE INSTALLATION

When there is more than one generator, it has been common practice with d-c installations to connect these generators in parallel. This in itself produces a voltage variation from no load to full load, since to ensure adequate load sharing it is necessary to provide the generators with a drooping voltage characteristic. The paralleling of d-c generators in aircraft has been a major problem, and a satisfactory solution has not yet been found. Figure 3 of the discussion shows a twin-engine installation with two generators supplying separate circuits. If either generator fails for any reason the relay R closes and automatically connects both bus bars to the remaining generator. A small "floating" accumulator is shown on the bus bar in Figure 2. As shown in Figure 3, there is no provision for ground supply. In Figure 4, an auxiliary generating plant has been added which, through normally closed contacts on relays $R1$ and $R2$, supplies all the loads on the ground. In flight, generator $G1$ supplies the loads connected to $L1$ and $G2$ supplies $L2$. As shown, if either generator fails it can be replaced by the auxiliary generating plant, which can be either hand-started or automatically started. If the remaining generator now fails, both $L1$ and $L2$ will be connected to the auxiliary generating plant. An alternative to the scheme shown in Figure 4 would be to repeat the layout shown in Figure 3 and only introduce the auxiliary generating plant for ground operation and extreme emergency in the air, that is, the loss of both generators.

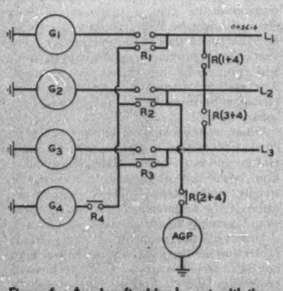

Figure 6. An aircraft-wiring layout, with three separate circuits, four main-engine-driven generators, and an auxiliary generating plant

It should be noted that in either case the auxiliary generating plant will not require special attention for high-altitude work, since it will only be used in the case of emergency. The arrangements shown in Figures 3 and 4 will maintain a much better voltage characteristic than is obtained by parallel systems, but will demand good overload capacity of the generators and will impose a limit on the size of motor which can be installed. Motor loads on twin-engine aircraft are not likely to be very heavy, and arrangements should be made to keep heavy motor loads on one circuit and radio and other loads sensitive to voltage variation on the other circuit.

FOUR-ENGINE INSTALLATION

In Figure 5 of the discussion, a four-engine layout is shown combining parallel operation and separate circuits. The main loads containing all motors and other equipment, are supplied by three generators, $G1$, $G2$, and $G3$, connected in parallel and feeding the bus bar $L1$. A separate circuit $L2$, supplied by one generator only ($G4$), is provided for loads sensitive to voltage variation; in this circuit all possible precautions can be taken to eliminate voltage disturbances, and this circuit also will carry the small "floating" accumulator. Loss of any one of the generators, $G1$, $G2$, or $G3$, will mean just that much less power to the main loads. The loss of generator 4 automatically will transfer the loads of $L2$ on to an auxiliary generating plant that can be started from the $L1$ circuit. Power supply on the ground for both bus bars will be obtained from the auxiliary generating plant with the switch marked "S" closed. Should both the auxiliary generating plant and generator $G4$ fail, the loads on $L2$ could be transferred automatically to $L1$, where they will receive a variable-voltage supply, which would be better than no voltage supply at all.

This arrangement permits heavy motor loads and easier paralleling, since a large drooping voltage characteristic is permissible on the parallel system. It is considered that this arrangement is a very good compromise and is well worth further development.

Table I. Connections of Loads and Generators With Automatic Load Transfer When More Than One Main-Engine-Driven Generator Has Failed

G1	G2	G3	G4	AGP
Failed	Failed	$L3$	$L1+L2$	
Failed	$L2$	Failed	$L1+L3$	
Failed	$L1+L2$	$L3$	Failed	
$L1$	Failed	Failed	$L2+L3$	
$L1$	Failed	$L3$	Failed	$L2$
$L1$	$L3+L2$	Failed	Failed	
Failed	Failed	Failed	$L1+L2+L3$	
Failed	Failed	$L3$	Failed	$L1+L2$
Failed	$L1+L2+L3$	Failed	Failed	
$L1$	Failed	Failed	Failed	$L2+L3$
Failed	Failed	Failed	Failed	$L1+L2+L3$

Generators are connected as in Figure 6.

Where large motors are required, such as for undercarriage operation, it would be a good plan to replace one large motor by two motors of half size, fed from separate generators and arranged so that either motor can complete the operation in twice the time taken by both motors operating together. With this arrangement, a considerable increase in safety factor is obtained at very slight increase in weight. By this means, also, the load circuit marked $L1$ in Figure 5 can be subdivided further. An aircraft-wiring layout readily divides itself into three circuits. Figure 6 shows a layout with three separate circuits, four main-engine-driven generators, and an auxiliary generating plant. Relays $R1$, $R2$, and $R3$, which have normally closed contacts, are energized by generators $G1$, $G2$, and $G3$, respectively, putting these generators onto the three separate circuits $L1$, $L2$, and $L3$. Failure of any one of these three generators automatically will bring generator $G4$ in its place through the relay $R4$. The relays marked $R(1+4)$, $R(2+4)$, and $R(3+4)$ are normally closed, but will be opened by supply from either of the generators corresponding to the suffix numbers. For example, $R(1+4)$ will only close if generators $G1$ and $G4$ are inoperative. It will be seen that power is maintained on all circuits even when all four main-engine-driven generators are disabled. A table is shown in Figure 7 which details the generators and their connected loads under all conditions of failure.

One of the circuits marked $L1$, $L2$, or $L3$ should be confined to loads sensitive to voltage changes or other special loads, and will be connected to the small "floating" accumulator.

CONCLUSION

The circuits shown herein are all reduced to their simplest form, and do not indicate the detailed requirements of circuits such as fuses. There is no new technique involved, and it is apparent that for medium-size aircraft not operating at extremely high altitudes, the 24-volt d-c system still is worthy of development. It is considered that to obtain the most from a low-voltage d-c system, it will be necessary to abandon the use of the accumulator and not to attempt to connect the generators in parallel.

In large aircraft where power demands are very heavy, an increase in voltage will be essential to reduce cable weights, and in this case the elimination of commutators and brush gear makes the use of a-c power inevitable. It is hoped that the foregoing remarks indicate that the paralleling of generators for d-c power systems is not necessarily the best arrangement. Hence when a-c power is used and paralleling becomes an even more difficult problem, it would be better to investigate fully the possibilities of separate circuits before any attempt is made at paralleling.

W. K. Boice and **L. G. Levoy, Jr.**: The tabulation in the paper comparing distribution copper weights for various voltage levels gives only an approximate idea of the relative weights of wiring. The weight of wiring is affected also by the weight of insulation, which increases with increasing voltage and offsets some of the savings obtained by reduction of copper weight. Moreover, mechanical-strength requirements establish minimum wire sizes, so that for circuits to small loads, increase in voltage will not in all cases permit a reduction in wire size.

Mr. Taylor has pointed out that aluminum conductors have less weight than copper conductors of equal conductivity. However, aluminum conductors have larger cross sections, and hence require a greater weight of insulation than copper conductors of equal conductivity. Thus the increase in weight of insulation offsets some of the conductor weight savings obtained by substituting aluminum for copper. The relative difficulty of making good connections with aluminum is probably the most important reason why it has not been used more widely. Although methods are available for making reliable connections with aluminum, they are relatively inconvenient to use, particularly for repairs in the field.

Mr. Powell calls attention to the fact that developments in equipment for aircraft use undoubtedly will result in many changes in equipment for other uses. This probably will be even more true for the case of equipment for very large planes than for present-day aircraft.

Mr. Concordia's discussion of governor characteristics points out the advantage of slip in the drives for a-c generators which are to operate in parallel. As he points out, time lag in the governing mechanism has been neglected in the curves given. This factor is of considerable importance, particularly with suddenly applied accelerations. In the case of constant-speed drives now being developed, the use of freewheeling is expected to be helpful in relieving the severity of requirements for governing equipment for parallel operation.

The paper states that constant-speed transmissions are necessary for main-engine-driven alternators that are to supply constant-frequency alternating current. It points out further that if the frequency is not held constant, difficulties in the operation of certain types of loads may be expected. In some British aircraft designs, constant-speed transmissions are eliminated and a-c generators are driven directly from main engines, nonparallel operation being used. When the engine-operating speed range is relatively narrow so that the frequency range is within the limits for satisfactory operation of electric loads, this method of operation may result in substantial weight saving and may eliminate governing problems entirely. However, on many aircraft this is not feasible because of wide engine-speed ranges and close frequency limits for certain types of electrical apparatus.

Editor's Note: This closure does not acknowledge Mr. Higgs' discussion which was received too late for the authors to submit comments on it.

Electric-Circuit Fault-Protective Principles as Applied to D-C Aircraft Systems

Discussion and author's closure of paper 44-38 by R. H. Kaufmann presented at the AIEE winter technical meeting, New York, N. Y., January 24–28, 1944, and published in AIEE TRANSACTIONS, 1944, pages 333–44.

R. J. Lusk (nonmember; captain, United States Army, Wright Field, Dayton, Ohio): Mr. Kaufmann's paper applying fault-protective principles to d-c aircraft systems is an important indication of the present trend toward a co-ordinated electrical system for use in aircraft and may well serve as a basis for development of standards for such a system.

However, the immense size and organization of military air forces in the present war and the distribution of these forces over a large portion of the world require careful consideration of several factors which must of necessity influence the design of an electrical system for present day military aircraft in addition to the usual requirements of safety, reliability, first cost, simplicity of operation, maintenance cost, and efficiency.

The first of these several factors is weight. The addition of even 100 pounds to the electrical system of a bomber would influence to a tremendous extent the capabilities of these aircraft, yet the addition of this weight in protective devices alone is easily attained in any system offering complete protection against possible electrical faults, without consideration for the additional structural appliances necessary to fasten cables and protective equipment in place.

Another important factor is the supply problem. The immense number of bases from which combat operations are conducted and the huge quantities of supplies which must be transported to these bases make it impossible to rely on stocks of small items such as fusible links. Systems in which selectivity depends on fusible links are potentially a problem since the insertion of a link of improper rating at any point in such a system usually destroys the selectivity of the system, rendering the system a hazard in combat.

The last and perhaps the most important factor of all is simplicity of maintenance. Electrical specialists in the Army Air Forces receive a bare minimum of instruction on the electrical system and no provision is made for personnel with technical background capable of advising electrical specialists in the field. This is probably a result of the minimum of trouble experienced with the small-capacity single-generator-and-battery systems used during the years of peace previous to the present war.

These additional factors of weight, supply, and extreme simplicity of maintenance require a careful accounting of the faults which may be encountered in aircraft and consideration of means to prevent service interruptions due to these faults with the purpose of eliminating any unnecessary protective equipment.

In general, short circuits to ground may be caused by careless maintenance when the system is energized, by normal wear and tear, by the effect of gunfire on equipment, or by the effect of gunfire on electrical wiring. Gunfire may also result in open circuits.

Short circuits during maintenance may be reduced in number if not prevented altogether, by the use of insulated junction boxes, so designed that tools cannot contact studs or other equipment at a positive potential and the metallic structure of the airplane simultaneously. Faults due to normal wear and tear can be minimized by proper and careful installation.

Any short circuit of serious magnitude may be isolated without insurmountable difficulty when the fault occurs on an individual feeder circuit by the use of a circuit protective device at the junction of the individual feeder circuit to the main feeder circuit. Adequate protection is thus furnished to the equipment portion of the airplane electrical system which, of course, provides the largest target or exposure.

Short circuits or open circuits in the main feeder circuit must not result in interruption of the flow of power to the important electrical loads connected to the main feeder circuit. Short circuits due to improper maintenance may be avoided by installations planned to make these faults impossible. The effect of open circuits due to gunfire may be minimized through the use of multiple-channel main feeder circuits, but short circuits due to gunfire cannot be so easily dismissed, indeed, the problem becomes even more difficult when it is realized that protection must be provided against short circuits occurring at junction boxes as well as on feeder cables. The problem is further aggravated because fault currents under minimum generating-source conditions may be less than starting currents under maximum generating-source condition, particularly if solid faults are to be disposed of in $^{1}/_{10}$ second as proposed by Mr. Kaufmann.

As pointed out by Mr. Kaufmann the attainment of a selective protective system relies on the judicious use of inverse time characteristics. A study of proposed systems indicates that a minimum of four channels must be used to obtain selectivity and that, even then, no protection for junction boxes is provided and the supply problem previously mentioned is incurred. Any system incorporating selectivity must probably be rejected because of these reasons.

Other systems making use of balanced current protection in transmission channels and ground fault current detection at junction boxes are objectionable because of weight, and extremely objectionable because of the maintenance difficulties which would certainly be encountered.

However, the problem of short-circuit protection on the main feeder circuits may not be so serious as to demand any form of protection since it has not been definitely established that faults on this part of the electrical system do not burn clear in a relatively short period. The short circuits caused by gunfire usually consist of a path to ground through a fractional part of the total small strands which make up aircraft cables and these quickly burn off leaving either a layer of cable insulation or an oxide film to prevent further current flow. For present-day military aircraft it appears that the main feeder circuit should not incorporate protective equipment. Mr. Kaufmann's recommendations concerning protective practice for individual feeder circuits, need only be modified for present-day application because of the unavailability of closely calibrated overcurrent and inverse-time-characteristic devices of a reset type. It is to be noted that a reverse-current circuit breaker is already being used in high-capacity aircraft generator circuits.

C. D. Hayward (General Electric Company, Philadelphia, Pa.): In this paper as well as in reference 1 of this discussion, the need for a d-c circuit breaker having a directional or reverse-current trip to provide adequate fault protection of the generator and battery circuits in d-c aircraft systems has been described. Mr. Kaufmann's paper also points out the advantages of such a breaker for parallel line protection.

Figure 1 of this discussion shows a new reverse-current circuit breaker especially developed for this service which is now in production. While particularly designed for generator protection, it is also suitable for battery and parallel-line protection as well. When used for generator protection, it is connected in the generator circuit between the generator control relay and the positive bus, one being used in the circuit of each generator. In case of a fault occurring in the generator, the generator relay or their connecting cables, this breaker will trip on the reverse flow of current from the bus to the fault interrupting the current and disconnecting the faulted circuit from the bus. At the same time an auxiliary switch on the breaker opens the field circuit so that the faulted generator itself cannot continue to feed current into the fault.

The reverse-current breakers in the other generator and battery circuits do not trip, because the current flowing through them to the fault is in the forward direction. Hence, the sound generators and batteries are left in service to supply power to the system immediately after the fault is cleared.

Figure 1. A new d-c reverse-current circuit breaker for aircraft - generator protection

This breaker is rated at 30 volts, 300 amperes, direct current on the basis of a 50-degree-centigrade temperature rise. High-temperature-resistant insulation materials have been used in its construction however, so that 400 amperes can be carried continuously and 800 amperes for one minute without damage. The reverse-current trip setting is normally adjusted to 300 amperes for the protection of generators of either 200- or 300-ampere rating. Other settings over the range of from 200 to 800 amperes are available for special applications. The current interruption rating is 8,000 amperes, which is ample for 28.5-volt aircraft systems employing up to as many as six standard 300-ampere generators and three storage batteries. Currents up to the full rating are interrupted in less than 0.015 second at any altitude from sea level to 50,000 feet.

A signal is provided to indicate when the breaker has tripped open. The breaker may be reclosed manually after tripping by pressing on the reset lever.

The reverse-current tripping mechanism is polarized by an Alnico permanent magnet with a special magnetic circuit to prevent demagnetization and consequent change in calibration on high fault currents.

The over-all dimensions are $1^{1}/_{2}$ inches wide by $3^{3}/_{32}$ inches high by 7 inches long. The total weight is 1.18 pounds. It can readily be mounted on any flat surface and operates in any position.

Extensive tests have been made to prove its operation under conditions of vibration, shock acceleration, temperature, air pressure, etc., incident to use in military aircraft. Official approval of its use in Army aircraft has been given by the equipment laboratory at Wright Field.

Reference

1. PRESENT D-C AIRCRAFT ELECTRIC-SUPPLY SYSTEMS, H. C. Anderson, Jr., S. B. Crary, and N. R. Schultz. AIEE TRANSACTIONS, volume 63, 1944, June section, pages 265-72.

J. C. Cunningham (Westinghouse Electric and Manufacturing Company, Pittsburgh, Pa.): Mr. Kaufmann's review of fault protective principles will be read with interest by many aircraft engineers because of the need for producing combat aircraft which will retain their fighting ability after damage to the electrical system from gunfire.

In applying these principles to the protection of aircraft electrical systems there is one major factor which must be given consideration which does not enter the picture in land electrical systems. That is, the extreme need for minimum weight. In selecting a method of protection for any particular part of the system it is first necessary to select one which will do the job satisfactorily. For some parts of the system there is only one answer. However in other zones, such as the cable circuits, there are several methods of solution, and both performance and weight must be considered.

Mr. Kaufmann suggests the use of balanced-current, directional-overcurrent, or inverse-time overcurrent protection for the cable circuits. The first two of these methods involve the use of magnetic relays, and circuit breakers. Inverse-time overcurrent protection requires only fusible links. For similar current-carrying and interrupting ability the weight of the link would be approximately $^{1}/_{10}$ that of the circuit-breaker and trip element.

In order to maintain service at least one more circuit than the number of faults must be provided. With the current-balance scheme selectivity can be obtained with this minimum number of circuits, but each circuit must consist of two cables, each capable of carrying one half the total load current. With inverse-time overcurrent protection it is necessary to provide two more circuits than the number of faults in order to get selectivity, but each circuit consists of only one cable of one half the total load current. Thus to clear two faults and carry 100 amperes load the current-balance scheme requires six number 10 cables or eight number 14 cables. The inverse-time overcurrent scheme requires four number10 cables or six number 14 cables. In either case the current-balance scheme is heavier and requires more circuits. In many cases this weight difference might be better spent in 0.50-caliber ammunition.

One advantage of the current-balance scheme over inverse-time overcurrent protection is the fact that it can be made more sensitive. In this respect the d-c aircraft systems using shunt generators are more critical than proposed a-c systems which have generators with separate exciters. In one typical d-c system the total fault current at the end of 0.1 second is only from one to two times the *total* maximum load current (depending on fault location and number of generators available). Under similar conditions a proposed a-c system will deliver from 2.5 to 16 times the maximum *total* load current. The provision of substantial battery capacity helps to maintain adequate current for clearing faults on d-c systems with shunt generators.

R. H. Kaufmann: A vote of thanks is to be extended to all of those who expressed their opinions on this subject in the form of a formal discussion. It is as a result of such effort that we are led to the most effective solution of the particular problem.

That there is need for an adequate fault protective system incorporating accuracy, simplicity, reliability, and fast operation seems warranted by the following considerations:

1. Abstract tests of fault phenomena indicate that uncontrolled electrical faults on 30-volt d-c systems present dangers; those which ultimately burn themselves clear present unnecessary fire hazard during the period of arcing; and those which weld themselves solid present a service hazard to the entire electrical system.

2. Analysis of electrical distribution systems in current use discloses the existence of numerous locations where the occurrence of persistent electrical faults would be disastrous to electrical-service continuity.

3. That some aircraft electric systems incorporate emergency electric lines, which normally lie idle, and are intended to be used if and when the primary electric system fails. It would be my opinion that a better over-all result would be obtained had this weight investment been applied toward the attainment of absolute reliability in the basic system.

Equipment weight must be constantly and vigilantly scrutinized in aircraft service. The justification for weight expenditure in a protective system is difficult to evaluate in the case of those ships which somehow manage to complete their mission. Their crews may have had to face unnecessary operating contingencies and the ships may have suffered some unnecessary damage as a result of inadequate electric-system protection, but quantitatively the values are intangible. Think for a moment, however, of those ships which fail to complete their mission. To what extent is that failure due to electric-system deficiency? A loss ratio of five per cent in present combat service is not uncommon. Suppose that a fully effective electric-fault protective system would permit this loss ratio to be reduced to four per cent.

Assuming a base pay load of 30,000 pounds per ship

1. With a five per cent loss ratio, the average delivered load per ship becomes 0.95×30,000, or 28,500 pounds.
2. With a four per cent loss ratio, the average delivered load per ship becomes 0.96×30,000, or 28,800 pounds.

In other words, the reduction in loss ratio from five per cent to four per cent is directly responsible for increasing the average delivered pay load per ship by 300 pounds. A weight investment of 300 pounds per ship to accomplish this improvement would thus be definitely justified in that it would result in no reduction in the delivered load per ship and would constitute a clear gain in the form of a return of one ship out of every 100 sent out, and what is more important the return of the operating crew associated therewith. The reduction in damage effected on all other ships constitutes another item of net gain.

Present D-C Aircraft Electric-Supply Systems

Discussion and authors' closure of paper 44-32 by H. C. Anderson, S. B. Crary, and N. R. Schultz, presented at the AIEE winter technical meeting, New York, N. Y., January 24–28, 1944, and published in AIEE TRANSACTIONS, 1944, June section, pages 265–72.

C. B. Mirick (Naval Research Laboratory, Anacostia Station, Washington, D. C.): I would like to offer the following comment on this interesting and valuable paper.

It is noted that a voltage drop of one-half volt is recommended for operating the equalizer circuit, and that this drop is obtained by inserting a resistance in series with the negative generator lead. The addition of this resistance is indicated even when a series field is present in the generator (see Figure 11 of the paper). While equalizing action is obtainable with existing regulators when half a volt is used, the action is much more positive and effective if this voltage is increased to one or $1^1/_4$ volts. This is indicated in Figure 1 of this discussion. When a series field is present this increased equalizing voltage can be obtained without loss of power by transferring the equalizer tap to the generator side of the series field. In this case the series resistor, r_3, usually can be omitted.

In Figure 8 of the paper a single lead from generator to voltage regulator carries both the shunt-field current and the current for the regulator coil. This arrrangement has been found undesirable in practice, particularly in large ships in which the lead in question may be of considerable length. It has been found desirable to keep the voltage-sensitive leads of the voltage regulator entirely separate from other circuits, all the way to the points of voltage regulation.

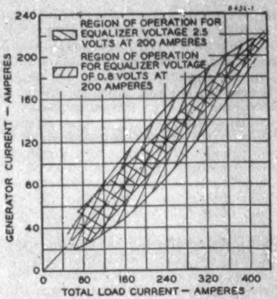

Figure 1. Division of load on aircraft generators operating in parallel

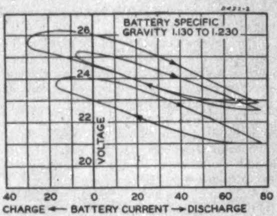

Figure 2. Battery characteristics as a function of state of charge

Generator voltage regulated at battery terminals

Regulator adjusted to the floating voltage for each condition

The generator and battery characteristic of Figure 7 of the paper is believed to be qualitative only. For short-time overload periods there will be considerable departure from the conditions as indicated. This departure will depend on the duration of load and the state of the battery. The possible range of such variation is indicated by Figures 2 and 3 of this discussion. It has been found that rapid changes in polarization of a battery undergoing transition between heavy charge and heavy discharge make the voltage very unstable.

This comment represents the personal views of the writer and does not commit the Navy Department in any way.

Joseph W. Allen (Bureau of Aeronautics, United States Navy Department, Washington, D. C.): The authors are to be complimented on their very excellent contribution to a subject of great interest. This paper will be read by many who are new in the aircraft field, and in the hope of adding to is effectiveness, I wish to submit a few remarks.

The title is somewhat misleading in that only one system, used largely by the Army Air Forces, is described. It is recognized that the Army Air Forces should continue use of the system, substantially as described by the authors, for the present emergency. It is doing an excellent job, is understood by the personnel that use it, and to introduce a modification at this time would doubtless do more harm than good. It is understood that the Army Air Forces is aware of the modifications that could be made. However, it is felt that the improvements applicable and other variations should be discussed, since the possible adoption of a similar system for commercial aviation in the postwar period is mentioned.

Figure 1 of the paper shows a four-engine system with a resistor for obtaining equalizer voltage between negative terminals of each generator and the common ground. Figure 8 shows a generator with a series field. Figure 11 shows a four-generator system with equalizer resistors for each generator and also series fields in each generator. In another widely used system the resistors are left out entirely. The equalizer coil connection is made between the series field and the generator brush, instead of the point shown in Figure 11, thus taking advantage of the voltage drop present in the series field under load conditions. The voltage drop in the series field is roughly two to four times that provided by a one-half volt external resistor, and by such relative magnitude the effects of ground resistance R_3 and R_4 are lessened. However, to decrease further the effects of R_3 and R_4 and, also, to provide a ready

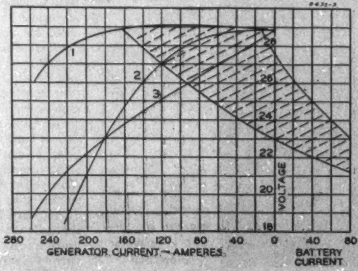

Figure 3. Regulation characteristics of generators and 12-cell aircraft battery

1. Normal operation
2. Fixed resistance of 2.66 ohms inserted in the field circuit
3. Equalizer grounded

Shaded area indicates region of battery operation

Specific gravity 1.120 to 1.230

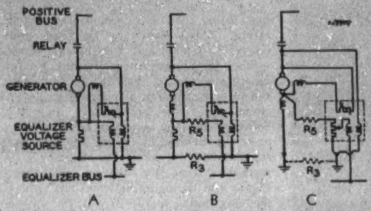

Figure 4. Comparative study of aircraft voltage-regulator conditions

A. Schematic diagram of the typical four-generator aircraft electric-supply system shown in Figure 1 of the paper

B. Connections of a four-generator aircraft electric-supply system, as shown in Figure 11 of the paper. Less R_1 and R_1'

C. Schematic diagram of the "improved" system, with an equalizing potentiometer added to the equalizer circuit at the voltage regulator

means for equalizing the load division between generators, an equalizing potentiometer is added to the equalizer circuit at the voltage regulator. This consists of a small tubular resistor with a positionable contacting clamp. The latter is shown in Figure 4C of the discussion. In Figure 4C, R_3 is shown as a resistance in the structure between the points at which the negative leads are bonded to the structure. Figure 11 of the paper tends to convey the impression that only one ground exists, whereas the negative leads of r_s (1, 2, 3, and 4) are actually grounded to the structure and resistances R_3 and R_4 are the resistances between such grounds. In Figure 4C, the equalizer voltage is obtained from the series field, and when a generator is supplying load a small part of the generator line current is shunted through the equalizing potentiometer to a common point, or ground, at the regulator. The drop of the equalizer coil lead, R_5, plus the drop in the equalizing potentiometer, is in shunt with the drop in the series field plus R_3. With adequate bonds of the generator negative leads to the structure (essential in all systems), the effects of R_3 and R_4 are neutralized, since the voltage for equalizing is that existing between the equalizer tap at each generator and the common ground for the equalizing potentiometers at the regulators. To balance the load division between generators all that is necessary is to adjust the contacting clamps of the equalizer potentiometers to a position such that, at proportionate generator loads, the voltages between regulator ground and equalizer potentiometer clamps are equal.

In Figure 11 of the paper, if it became necessary to install a generator of larger or smaller capacity, the resistor r_s would have to be carefully adjusted or changed, whereas, in Figure 4C, the contacting clamp on the equalizing potentiometer of the changed generator would be set to the required value.

Another variation resulting in improved operation with any number of generators, is indicated in Figure 4C. This results from the manner in which the regulator-coil and generator-field leads are connected to the positive of the system. When the regulator-coil and field currents are carried by one lead, as in Figure 11, the variation of field current produces a variable drop in the combined lead, and unless care is taken to make the combined lead of large cross section where long runs are necessary, the bus voltage may rise to a dangerous value above the voltage held by the regulator. By providing separate leads for the field current (variable) and the regulator-coil current (constant), respectively, the voltage held by the regulator is not affected by the variable field current, and the voltage held is constant. Even if it is necessary to connect both leads at the same point of the positive circuit, use of two leads results in a lower weight and better voltage regulation because a greater drop in the field lead can be permitted. For very short runs it is practicable in some cases to use a combined, heavier lead at the expense of an acceptable voltage variation for the sake of eliminating a wire.

The use of a common lead between the positive bus and generator 2 of Figure 11 also is productive of complications. By providing separate leads between each generator and the bus, the factor R_3 (and resulting complications of increased conductor weight plus effect on voltage regulation) is eliminated. Reference to a table showing recommended current-carrying capacities of conductors will indicate that a conductor with $2N$ circular mils is not recommended for use at double the amperes recommended for a conductor having N circular mils. The recommended practice in single conductors is to provide for a current of 400 amperes, for example, or three times the circular mils that is recommended for 200 amperes. This fundamental indicates that for aircraft, especially with "unbunched" open wiring, a weight saving could be obtained by paralleling cables for higher currents, and at the same time reduction in the number of cable sizes that need to be manufactured and stocked would be effected.

The authors, in discussing "Generator Regulation Characteristics," call attention to the undesirable features of a generator with a rising characteristic. Actually the operation of a generator with a rising characteristic depends to a large degree upon the characteristic of the voltage regulator used. If the regulator has the required rapid response, it will reduce the shunt field and maintain the desired voltage before any harm can result, even when overloads are applied to an open-circuited generator. Care must be taken to assure, at extreme overloads and high generator speeds, that the generator voltage will be held at the desired voltage by the regulator response and the high value of resistance available in the voltage regulator. It is granted that a series generator characteristic is to be avoided, but a judicious amount of cumulative compounding need not be a detriment.

In directing attention to the advantage of a drooping generator regulation characteristic, the authors lead the way to another system, a system wherein the voltage at the bus "droops" with load. Such a "droop" may be obtained by suitably energizing the present equalizer coil, or its equivalent, in proportion to the generator load, rather than only when an unbalance of load between generators exists. The "droop" can also be obtained by having sufficient resistance in the positive lead between the voltage-regulated point and the bus. A combination of the load-actuated coil on the regulator and voltage drop in positive lead is also practicable. Such a "droop" introduces a stabilizing action to the system and enhances load sharing between the generators. The authors, in fact, show a "droop" in bus volts in Figures 6, 7, and 12 of the paper. A rough check indicates the "droop" at full load to be approximately one volt. The presence of a "droop" in the system is desirable. It is an inherent stabilizing function, and is employed in many devices; its value is not confined to the voltage of an electric system.

In Figure 5 of the discussion, the high-speed generator volt-ampere characteristic, with battery, is dotted in. The dash-dot line indicates a possible bus "droop" characteristic wherein the bus voltage reaches the battery voltage at loads above 150 per cent. The "droop" shown is not necessarily the recommended droop, but is a "droop" that will give very good parallel operating characteristics, and is merely shown for the purpose of discussion. It may be of interest to point out that the "droop" as a means for obtaining proportionate division of load between generators has been in use for a long time. In the early twin-engine airplanes the voltage was held by the regulator at the input to a control panel. Since the generator outputs at 14.5 volts were 50 amperes or less and the airplanes were of wood, the positive and negative generator leads were carried through the control panel. In the positive side was the reverse-current-relay series coil. The sum of the voltage drops in the positive lead, the series coil, and the negative lead between control boxes and the common distribution bus was sufficient "droop" to assure parallel operation. In commercial planes a regulator box was used having current-limiting and reverse-current relays. Here again the drops in the series coils of the current-limiting and reverse-current relays, plus

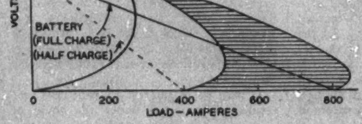

Figure 5

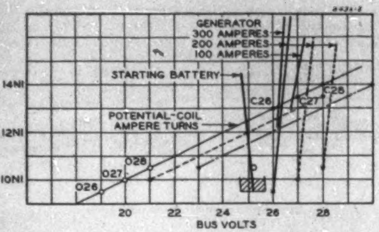

Figure 6

positive line resistance between regulator and bus, assured parallel operation. In Naval aircraft the control box had a line filter choke with a reverse-current relay between the control box and bus. Here again "droop" produced parallel operation. There are transport planes flying today in which "droop" provides load division between the generators.

The use of equalizer coils naturally entered the picture when generator currents increased to a value where it was no longer feasible to carry line current through the regulator box. The drop in the series coil of the reverse-current relay was reduced because of heating, and the drop in the positive line between regulator location and distribution to the bus was reduced. At this time, too, a constant voltage at the bus was requested. The equalizer coil filled the requirements.

The use of a "droop," permitting the battery to supply current for very high peak demands, especially at high generator speeds, where overloads produce most harm to commutators and brushes, should increase generator life. Hence, since the bus voltage is not constant, as evidenced by Figures 6, 7, and 12 of the paper (and the holding of a constant bus voltage is a practice not normally advocated in systems subject to variable demand), perhaps a greater utilization of the "droop" for parallel operation is practical, as is the use of wood in high-speed high-power military aircraft. The use of the "droop" would materially improve operation at all loads even though the voltage-regulator inaccuracies or dissimilar voltage-regulator characteristics existed.

A slight modification of Figure 7 will bring out an important characteristic of a generator–battery system. The open-circuit voltage of a 12-cell storage battery may be taken at 25.0 volts, for practical purposes. The initial current under a short-circuit condition for a fully charged 34-ampere-hour aircraft battery is substantially as shown in Figure 7, approximately 800 amperes. A fully charged storage battery has a uniform decrease of voltage with increasing values of initial discharge rates, each such discharge rate being from a fully charged battery. Such a characteristic is represented in Figure 5 of the discussion, by the voltage line starting at 25 volts, open circuit, and reaching zero volts at 800 amperes. The modification of Figure 7 is made by indicating the voltage at which the storage battery current becomes available for addition to the volt-ampere characteristic of the generator. It is of value at lower-than-rated voltage and generator speeds where a sizable output from the generator at low-engine rpm prevents discharge of the battery.

I found Figure 9 of the paper a little difficult to read. The 26- and 27-volt relay characteristics tend to convey the impression that reverse currents persist to zero bus voltage. Figure 9 probably is intended to apply only to the case of a bus powered by a generator and an incoming generator. The indicated divergence between the slopes of the 26- and 27-volt pickup circuit characteristics is confusing.

To present the characteristics in a slightly different, and, it is hoped, easier-to-read manner, Figure 6 is presented. The potential-coil characteristic is shown by an ampere-turn versus bus-voltage line. To have the relay close at a desired voltage, spring tension is varied. When such a relay closes at 26 volts it will open when, for example, the voltage is decreased to 19 volts. The closing and opening points *C*26 and *O*26 are so indicated. The closing and opening points at 27 and 28 volts are indicated by similar notations.

The line "storage battery" indicates the reverse *NI* that results when the generator voltage falls below the bus voltage with only a fully charged battery on the bus. The line is started at the *NI* corresponding to the 19 volts, at which voltage the relay will open without reverse current when its closing point is 26 volts. For a 28-volt closing value the opening voltage would be 21 volts, and the battery reverse *NI* line would be started at the *NI* indicated by the circled dot just above the hatched portion. The increased volts to close is obtained by increasing the tension of a spring holding the contacts open. The opening voltage (*NI*) is also higher because of the increased spring tension.

This shows, perhaps more clearly, why less reverse *NI* from the battery is required when the relay is adjusted to close at a higher voltage. The base of the hatched area indicates the possible range of open-circuit battery voltage under the conditions of temperature and electrolyte concentration. The storage battery reverse *NI* line would slant more to the left for increased discharge conditions of the battery.

For cases where a generator comes in on a bus powered by a voltage-regulated generator, conditions are governed to a marked degree by the generator ratings. As the rating of the generator increases the resistance of the generator circuit decreases. It is therefore evident that, for a given difference between bus and closing voltage, larger currents will flow with generators of high rating than with generators of lower rating. This is shown by the solid "generator, 300-, 200-, and 100-ampere" lines.

Since the reverse current flowing from a generator-energized bus to an incoming generator is a function of incoming-generator circuit resistance and voltage difference between incoming generator and bus voltages, the increase in closing point of the relay should not affect the magnitude of reverse current for chatter conditions. Figure 9 of the paper indicates that where the relay closing point is 27 volts the reverse current to initiate chatter is lower than when the closing point is 26 volts. In Figure 6 the dashed, and dashed–dotted lines parallel to the potential-coil-ampere-turns line have been drawn apart only to show more clearly the closing and opening ampere-turn conditions when closing points are increased to 27 and 28 volts, respectively. To study the effect of relay closing points on chatter, the dash and dash–dot 200-ampere generator circuit characteristics have been added. It is pointed out that the 26-, 27-, and 28-volt pickup lines for the 200-ampere generator characteristic are started at the opening *NI* values of the potential coil for the respective closing voltages. By inspection the reverse *NI* to initiate chatter (open the contacts) should not be affected by the closing voltage, and are governed solely by the reduction of *NI* necessary to open the relay.

Other details of operation modify the above discussion somewhat. However, the details belong properly to a paper on relays and not in a discussion of aircraft d-c systems.

R. H. Kaufmann (General Electric Company, Schenectady, N. Y.): The authors of this paper deserve particular credit for consolidating in one manuscript, with "*Reader's Digest*" brevity, a vast amount of fundamental knowledge pertinent to aircraft electric power-supply systems.

The topic of load division should have received greater emphasis in my opinion. The degree to which perfect load division is approached establishes the degree to which the combination of generators work as a team to accept load demands without overstressing any one unit. Generator-relay chatter, which has been an irritating difficulty, is directly the result of inequalities in load division. Observation of generator-current balance in service discloses variations of much greater magnitude than desired or necessary.

Not only is it necessary to adopt a sound system design arrangement inherently capable of providing good current balance, but component parts must be applied in such manner as to impart a reasonable degree of permanence to the controlling constants. For example, equal generator-negative-equalizer shunt resistance is accepted as a prerequisite to satisfactory current balance, but special attention must be given to connections and grounding terminal if permanence is to be acquired in this equalizer potential drop. Field maintenance personnel can be expected to keep the system in better condition if irregularities in current balance are rare occurrences rather than the usual thing.

It may be of interest to note that test results, as reported under the topic of

"Short-Circuit Currents" were obtained on a facsimile system containing only generators and batteries. It should be recognized that initial maximum fault-current magnitudes, as experienced in service, may be increased considerably because of the contribution of current from rotating shunt- or compound-wound load machines on the electric system.

H. C. Anderson, Jr., S. B. Crary, and N. R. Schultz: Both Mr. Mirick and Mr. Allen have pointed out the use of higher equalizer voltage for Naval aircraft, with the equalizer coil connection being made on the generator side of the series field. This practice is admittedly better from a performance standpoint, since it provides a higher equalizing voltage with no increased power loss. Army aircraft use an equalizing voltage-drop external to the generator in order to provide interchangeability of generators of different manufacture and design.

The use of two separate leads from the generator positive terminal to the voltage-regulator coil and the regulating resistance is favored by Mr. Mirick and Mr. Allen. The separation of the voltage-sensitive element of the voltage regulator from the field circuit undoubtedly has some advantages over the use of a single large conductor for both the voltage-regulator coil and the regulating resistance. However, the advantages of this method are not particularly great, the decision depending on considerations of reliability, weight, simplicity, and ease of installation.

Mr. Mirick's comments on battery characteristics are of interest, and he is correct in pointing out that the state of charge of the battery influences the volt-ampere characteristics of the system. The variations in battery characteristics, however, do not alter the volt-ampere characteristics appreciably and do not change the conclusions of the paper. The data presented in the paper were based on the assumption of a constant internal battery voltage of 24 volts and a constant internal resistance of 0.03 ohm, corresponding to 800 amperes on short circuit.

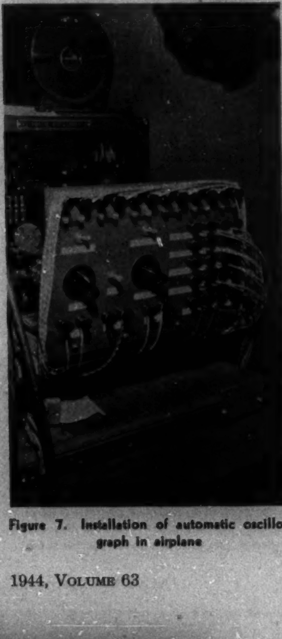

Figure 7. Installation of automatic oscillograph in airplane

The use of regulator "droop" to obtain parallel operation has been mentioned by Mr. Allen. Although this method is possible, it requires the use of more carefully adjusted and calibrated regulators. It is believed that the equalizer bus scheme results in a more practical and versatile method of obtaining parallel operation under the severe conditions of military aircraft duty. These desirable features undoubtedly will be considered important for commercial aircraft also.

Mr. Allen suggests that proper parallel operation may be obtained by means of voltage-regulator action with generators having rising characteristics. Any advantage of this type of operation is questionable, since it becomes extremely difficult to obtain stable parallel operation for all conditions in the wide range of system speed and load which are met in the operation of military aircraft.

It appears desirable to explain our analysis of reverse-current-relay chatter to help clarify the point raised by Mr. Allen. In Figure 9 of the paper, the "circuit characteristics" show the amount of reverse current which is delivered from the energized bus to the incoming generator, the incoming-generator voltage being 26 volts for the one curve and 27 volts for the other. It is seen from Figure 9 that for equal voltage on the bus and on the incoming machine no reverse current flows.

Once the reverse-current relay is closed, it is obvious that the relay voltage and bus voltage are equal. Keeping this in mind, let us examine the curves in Figure 9 marked "relay characteristics." These characteristics show for a given bus voltage the amount of reverse current required to open a relay (once it has closed on the bus) that previously has been set to pickup at an incoming-generator voltage of 26 volts for the one curve and 27 volts for the other. Since for these curves the relay is closed, it should be kept in mind that the relay and bus voltages are equal.

Figure 8. Laboratory equipment for electric-system tests

Thus, when the relay closes at an incoming-generator voltage such as to give more reverse current than is required to open the relay at that bus voltage and pickup setting, the relay will open immediately after closing. The persistence of this action is known as "chatter." The shaded areas of Figure 9 indicate which conditions will or will not cause chatter, the 26- and 27-volt designations merely indicating the incoming-generator voltage that will cause the relay to close.

Mr. Kaufmann points out that the short-circuit current contribution of rotating-load machines may be appreciable and may therefore increase the maximum initial fault currents shown in the paper. On the other hand, arc-resistance drop at the point of fault (see reference 3 of the paper) will tend to reduce the magnitude of the short-circuit currents.

A number of individuals have asked how information for the paper was obtained. Three methods of investigation were used:

1. An analytical approach, in which the characteristics of the complete system were calculated from the characteristics of the individual components of the system.

2. Experimental analysis in the laboratory, in which use was made of test equipment consisting of four aircraft generators and their associated relays, regulators, batteries, and loads.

3. A field investigation consisting of flight tests and ground tests.

The laboratory and field studies were carried out in parallel with the analytical work so that a constant check was maintained between the three methods. For the flight testing an automatic oscillograph was found to be of considerable value. Figure 7 of the discussion shows the installation of the automatic oscillograph in a four-engined airplane. The application is unique in that the oscillograph is designed primarily for central-station work, but was very easily adapted to fit the requirements of flight testing on aircraft electric systems. Laboratory tests were carried out on the equipment shown in Figure 8.

Lightning Protection for Rotating Machines

Discussion and authors' closure of paper 44-17 by G. D. McCann, E. Beck, and L. A. Finzi, presented at the AIEE winter technical meeting, New York, N. Y., January 24–28, 1944, and published in AIEE TRANSACTIONS, 1944, pages 319–33.

E. R. Whitehead (Duquesne Light Company, Pittsburgh, Pa.): The authors have performed a valuable service to the protection engineer in making available such complete information in one paper.

The conditions investigated correspond closely to those which commonly arise in practice, and the solutions proposed are in general practicable. For example, multiple line arresters may be used at very low cost under circumstances in which the installation of an overhead ground wire together with high impulse strength and low ground resistance would be entirely out of the question.

Another effective substitute for the overhead ground wire is the lumped inductance or "choke coil" with line-side protector tube grounded to the station ground. As the authors point out, however, this method is of somewhat limited application.

Variations of the problem sometimes arise in which the use of an insulating transformer of 1/1 ratio provides the best solution. In one such case, a low-capacity single-phase railway-signal circuit fed from a 2,300-volt three-phase rotating-machine bus, was responsible for several costly machine-winding failures. Although conventional machine protection was planned for the bus, it was deemed desirable to secure the best possible protection by removing the direct connection for surges. An insulating transformer of the five-kilovolt class with line-side arresters grounded to the station ground limits the surge to the electrostatic component which is easily absorbed by the bus protection.

A similar protection system has been applied very successfully to telephone and control circuits.

W. J. Rudge (General Electric Company, Pittsfield, Mass.): McCann, Beck, and Finzi are to be complimented upon their paper, "Lightning Protection for Rotating Machines." Their paper includes the basic scheme for protecting rotating machines presented to the Institute by Boehne,[1] and also by Beck and Fielder[2] at the AIEE summer convention in Toronto, Ontario, Can., in June 1930, and by Rudge, Weiseman, and Lewis[3] at the winter convention of the Institute in January 1933.

There are certain points in the paper, however, which require further amplification, particularly with regard to the use of choke coils (lumped inductance) as an alternate to the use of the surge impedance of the line to limit the lightning-discharge current. Our company has supplied a large number of protective equipments for protecting rotating a-c machines over the past ten years and in a number of cases has made a thorough study to determine whether choke coils could be used to advantage. Out of these studies there has been only one case where choke coils actually were supplied.

Our studies disclosed that practical application problems, such as greatly increased cost for coils, particularly for medium and large generators and stations having large short-circuit currents, which the coils must withstand, the necessity of factoring future additions to the generating capacity of the station; difficulties of installing the coils in one or more parallel three-phase feeders, will often penalize seriously the economics of rotating-machine protection over the scheme using the surge impedance of the line, arresters, and capacitors which proved so effective over many years.

A second point of interest is the use of the new type-*A* deion arrester described in the paper. It is stated that the new arrester has some advantage over the valve type when used in conjunction with the surge impedance of the line and the capacitance, because of its low *IR*-drop characteristic. We recognize the value of having a low *IR* characteristic for the line arrester and for this reason have advocated the Pellet arrester because of its low *IR* characteristic.

There are other factors which should be mentioned, such as the erosion life of the deion type-*A* arrester, particularly where it is given an unlimited interrupting rating and therefore may be used at locations having a large short-circuit current. In such locations, the erosion of the fiber elements will be greatest for each operation of the device. Since protective devices on low-voltage circuits operate with greater frequency, and since the system short-circuit currents determine the rate of erosion, the question naturally arises as to how many operations this arrester will withstand before it fails to clear the system follow current and thereby produces permanent fault on the system.

Obviously, valve-type arresters are not subject to this same limitation.

Our experience with the use of arresters and capacitors to protect rotating a-c machines over the past ten years has been excellent. In many cases where repeated failures have been experienced, the installation of the conventional protective equipment has eliminated the periodic failures of machines and has more than justified its initial cost in reduced maintenance expense, not to mention the lost service while repairs are being made.

We hope that this paper by McCann, Beck, and Finzi will encourage machine users to realize the importance of providing adequate protection for their machines.

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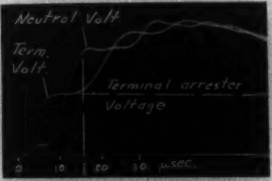
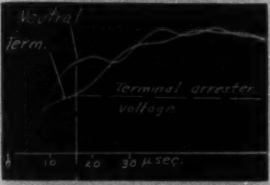
3. PROTECTION OF ROTATING A-C MACHINES AGAINST TRAVELING WAVE VOLTAGES DUE TO LIGHTNING, W. J. Rudge, Jr., R. W. Wieseman, W. W. Lewis. AIEE TRANSACTIONS, volume 52, 1933, pages 434–65.

G. D. McCann, E. Beck, and L. A. Finzi: Mr. Rudge has pointed out that the methods of machine protection recommended during the past ten years have on the whole been quite satisfactory. The authors agree with this and have pointed it out in the paper. The principal object of this paper has been to present the more effective and economical methods of protection that have been made possible by the recent increased knowledge of lightning phenomena. In this connection the authors would like to emphasize the importance of the recommendation for the use of arresters and capacitors in the neutral of ungrounded and impedance-grounded wye-connected machines. Figure 1 of this discussion shows additional data recently obtained on the voltages that can appear at the neutral of an ungrounded 30,000-kva 13.8-kv turbine generator when a neutral arrester and capacitor are not provided. If this is compared with the voltage oscillograms in Figure 6 of the paper, it is seen that higher neutral voltages are obtained for this machine than for the smaller one previously tested. Even for a terminal voltage having a 17-microsecond front the neutral voltage can approximately double.

The use of choke coils in place of a section of overhead line for the wave-sloping mechanism are discussed by both Mr. Whitehead and Mr. Rudge. Their advantages and



A. Terminal voltage sloped to 8 microseconds



B. Terminal voltage sloped to 17 microseconds

Figure 1. Oscillograms showing voltage that can appear at the unprotected ungrounded neutral of a 30,000-kva 13.8-kv 3,600-rpm turbine generator

Surge applied to all three phases. Surge impedance of one phase winding about 120 ohms. Electrical length ten microseconds disadvantages have been pointed out in the paper. In the past, as stated by Mr. Rudge, they have been somewhat limited in their use by high cost and difficulty in proper design. This has been due principally to the high inductance that was required (175 to 350 microhenrys). The use of choke coils has been studied further, because it was found that, when short lengths of cable are connected between the overhead line and machine terminals and the type-*A* deion arrester is used as the line arrester, the necessary choke-coil inductance is very greatly reduced (see Table V of paper).

In connection with Mr. Rudge's comment on the low discharge characteristic of the Pellet-type arrester, we would like to point out that the significance of the very low discharge characteristics of the deion arrester lies in the fact that it is only a fraction of the discharge voltage of any valve-type arrester whereas there is only a relatively small variation between the various types of valve-type arresters. Mr. Rudge has pointed out that the erosion characteristics of the deion arrester limit its life, while there is less wear in a valve-type arrester as long as failure does not occur. This has been recognized, and an extensive study has been made on the expected life of the new type-*A* deion arrester in this type of application. The results indicate that the life should be of the order of 15 to 20 years.

In the use of a 1 to 1 transformer for supplying the necessary inductance for wave sloping, Mr. Whitehead has described a very interesting and effective method of economically obtaining surge protection for small machines.

Steady-State Stability of Synchronous Machines as Affected by Voltage-Regulator Characteristics

Discussion and author's closure of paper 44-12 by C. Concordia, presented at the AIEE winter technical meeting, January 24–28, 1944, and published in AIEE TRANSACTIONS, 1944, May section, pages 215–20.

C. F. Wagner (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): While the paper by Mr. Concordia is doubtless correct for the particular assumptions made, the conclusions drawn are much too general for the actual analysis presented. The author should have stated specifically whether the general conclusions drawn were backed up by further analysis not given in the paper or by tests devised especially to establish these points. Whereas tests are referred to, no actual data are given in the paper. For example, I am inclined to agree with Mr. Concordia that amortisseurs have little effect upon the power limit, but I fail to find the evidence in the paper upon which this conclusion is based. Similar considerations apply to the effect of antihunt devices in the regulator circuit.

The statement is made that "an important application of the analysis is the case of electric ship propulsion." Would not the actual speed–torque characteristic of a propeller in water be sufficiently different from the constant shaft torque assumed as to make questionable the direct applicability of the results to this case without at least further discussion or further analysis of the problem?

Governor characteristics, speed–torque characteristics of the load, effects of line resistance, and amortisseurs might well combine in their effects upon hunting to vitiate some of the conclusions drawn. Also, it is not clear without further discussion and analysis whether or not much smaller power limits might not result from this combination of effects. Amortisseurs would be of benefit in alleviating the hunting condition.

R. D. Evans (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The fact that voltage regulators can increase the steady-state stability limit of systems with synchronous machines until recently has been a phenomenon of theoretical rather than practical interest. Under certain conditions, however, advantage of this phenomenon can be utilized with electric systems for ship propulsion. This application is the basis of recent interest in the subject. Mr. Concordia has presented his method of estimating the steady-state stability limits of synchronous machines controlled by voltage regulators. This is worth while in itself. However, a brief review of the state of the art would not only serve to present the subject paper in relation to previous work but would also be useful to those who may be interested in the subject, since previous work has included the results of several important tests.

The theoretical possibility that a voltage regulator could increase the steady-state stability limit of a synchronous machine system was first suggested by E. B. Shand.[1] Evans and Wagner[2] demonstrated by analytical methods that such increases were possible for a transmission system equipped with commercial synchronous machines and voltage regulators, and in the closing discussion submitted results of laboratory tests[3] that verified the principle.

About two years later R. E. Doherty,[4,6] and C. A. Nickle and R. M. Carothers[5,6] presented results of tests on two synchronous machines connected directly together and operated with a voltage regulator. Their test results showed ratios as high as 3.5 for the stability limits with a regulator to that with fixed excitation. J. H. Ashbaugh and H. C. Nycum[7] presented the results of miniature-system tests using synchronous machines connected through a wide range of line reactance. Their results showed ratios as high as 3.5 for the stability limit with voltage regulators to that with fixed excitation for the condition of zero line reactance. These tests were made with a voltage regulator of a type first marketed about 1919. These tests also showed that slow-response exciters gave lower stability limits but that little was gained by using response rates greater than about 2.0 on a per unit basis.

Mr. Concordia gives the increase in the stability limit obtained by a regulator as about 60 per cent (as shown in Figure 5) for the case of two machines directly connected. It would be of interest if Mr. Concordia would explain why the ratios are so much lower than those shown by Doherty, Nickle, and Carothers.[4,5,6]

It is of interest to observe that the steady-state stability limits are obtained in the analytical method by approaching the problem from the standpoint of a transient analysis. The present paper gives little attention to the visualization of the underlying phenomenon. For this reason references to earlier papers 1–8 may be helpful.

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7. System Stability With Quick-Response Excitation and Voltage Regulators, J. H. Ashbaugh, H. C. Nycum. *Electric Journal*, October 1928, page 504.

8. Static Stability Limits and the Intermediate Condenser Station, C. F. Wagner, R. D. Evans. AIEE Transactions, volume 47, 1928, pages 94–121.

J. G. Holm (Boston Port of Embarkation, Boston, Mass.): I should like to make a few comments of a very general nature. As the author states in his paper, the purpose in taking an old subject for discussion is to present a direct method of calculating the gain in the steady-state stability when the voltage regulator is used.

The author points out that his method is especially applicable for the design of electric ship propulsion where the steady-state stability problem should be considered.

Having recently come in contact with this problem, I will say that the author is certainly correct in pointing out the particular application of his method. It seems to me, however, that the author's paper would have received more attention among the marine electrical engineers had it given a couple of examples illustrating the application of the method to problems of ship propulsion. The attention of a marine electrical engineer could be attracted by wording the title so as to disclose in it a connection with marine electrical problems. As it is, I am afraid that the marine electrical engineer will not even look the paper over on the ground that the steady-state stability problem does not come within the province of his activities. This valuable paper will probably not be seen by those for whom it is intended and to whom it should be of great value.

Since no changes in the paper may be made at present, I should like to suggest that the author write another paper on the subject with a title as discussed above, to be published in some widely read electrical publication. The paper should contain examples on the application of the method, arranged in such a fashion that it be evident that the electric machinery involved is a part of a ship's installation. In these examples, the electric machinery should be identified first by their basic data, such as voltage, kilowatts, amperes, rpm's, and power factor, and then by direct- and quadrature-axis reactances and moments of inertia.

Such a paper will bring the method proposed by the author to the attention of a far greater number of electrical engineers. It should result in a wider application of the method, and consequently will bring the author greater satisfaction for the valuable work he has done.

C. Concordia: In reply to Mr. Wagner's question as to the basis for the conclusions in those cases wherein supporting calculations are not shown in the paper, we refer him to the statement on the first page of the paper that these additional conclusions are derived "from additional calculations which have been made." It would have been quite impossible to include all the calculations which have been made in connection with this study of machine stability and we did not feel that it was worth while to show any but a few typical curves as examples.

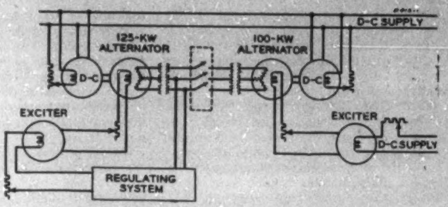
All of the effects discussed by Mr. Wagner, namely, governor characteristics, load speed–torque characteristics, line resistance, and amortisseurs have been investigated by calculations similar to those shown in the paper, and were taken into consideration in arriving at the conclusions stated. Moreover, the general method of attack has been justified by field experience with equipment designed in accordance with the principles outlined, and has been verified by factory tests. Figure 1 of this discussion shows a schematic diagram of the test setup used. It is seen that the two synchronous machines tested were mechanically connected to d-c machines, forming a feed-back circuit supplied from the d-c power system. The speed–torque characteristics of the d-c machines were varied during the course of the tests.

We are grateful to Mr. Evans for his discussion of some of the historical aspects of the study of the effect of voltage regulators on synchronous-machine stability. As stated in the paper and in Mr. Holm's discussion, the object of the paper was to present a method for calculating the gain in steady-state stability limit, taking the relevant factors into account more completely and directly than had been done previously.

One of Mr. Evans' objects is stated as being "to present the present paper in relation to previous work." To further this objective it may be well to point out that he is discussing not the history of the subject of the paper but of a related subject, namely, so-called "artificial" or "dynamic" stability. To quote from his earliest reference:

> "If a vibrating regulator of a much quicker degree of response were devised, an entirely different state of stability might be reached. This may be considered as follows—When the actual limit of stability is reached a condenser at the end of the line will commence to drift out of step at a rate determined by the excess load, and the voltage will drop correspondingly. A sudden increase in field excitation materially increasing the voltage would bring the rotor and voltage phase angle back again, tending to cause an overshoot in the forward direction and consequent high voltage. A reduction of field current will result in the initial condition being regained, followed by *the repetition of the cycle*."

Figure 1



It is also of interest to note that in a very recent publication, *Electrical Transmission and Distribution Reference Book*, 1943, page 189, it is stated that:

> "When synchronous machines are operated with voltage regulators, the stability limits of the system are importantly changed from the values which obtain for hand control. This phenomenon has been designated "dynamic stability with automatic devices." Its simplest mechanical analogy is that of a wand maintained in equilibrium by the action of a juggler. Dynamic stability on power systems is made possible by the action of voltage regulators that are capable of increasing or decreasing flux within a machine at a faster rate than that caused by the system in falling out of step. When the inherent stability limit is exceeded, both the mechanical system and the electrical system are maintained in a *continuous state of oscillation* through the development of restoring forces equal to or greater than the disturbing forces."

Now it is evident that the paper under discussion is not concerned with such continuous oscillations. The steady-state stability limit of the system including the voltage regulator is not different in kind from the steady-state stability limit of the system without voltage regulators; it is merely different in magnitude for the obvious reason that the system being considered is different in that it has additional elements. There is no continual oscillation merely because the limit without voltage regulators has been exceeded. In fact the limit without voltage regulators has no direct significance in the performance of the system including a voltage regulator. This conclusion, which is apparent as soon as it is stated, has been borne out by test experience. Another possibility, of course, is that Mr. Evans is discussing the same kind of stability as is described in the paper but has a misconception of its underlying phenomenon.

We are somewhat discouraged by Mr. Evans' observation that the paper gives little attention to the visualization of the underlying phenomena, as we had intended the section of the analysis entitled "Preliminary Remarks" to cover these points. In any event, it is believed that the practical engineer will be more interested in having a method of calculation leading to quantitatively correct results than in having a physical picture of the phenomena which tell him only qualitatively what is going on. We may even go further and remark that a great virtue of the mathematical method is that it makes possible the analysis of complex systems as soon as one understands the few really basic laws, and does not require that one visualize physically and simultaneously the whole system performance. Such attempts to analyze complete systems without the aid of mathematics often meet with success but sometimes lead one astray. The real underlying phenomena are only the very simple voltage and torque relations.

In regard to the magnitude of the increase in the stability limit, this depends greatly on the machine reactances. As stated in the paper, it is somewhat less than that obtained "by replacing the direct-axis synchronous reactance by the direct-axis transient reactance in the salient-pole synchronous-machine power-transfer formula." The machine reactances used are given on the curves, so Mr. Evans can very easily check the reasonableness of the gains obtained.

In connection with Mr. Holm's discussion, we may say that the method has been successfully used in the author's company for the prediction and improvement of the performance of marine propulsion equipment.

Regulation of A-C Generators With Suddenly Applied Loads

Discussion and authors' closure of paper 44-15 by E. L. Harder and R. C. Cheek, presented at the AIEE winter technical meeting, New York, N. Y., January 24–28, 1944, and published in AIEE TRANSACTIONS, 1944, June section, pages 310–18.

H. G. Rickover (captain, United States Navy, Bureau of Ships, Washington, D. C.): The problem of voltage regulation of a-c generators under suddenly applied loads is of considerable importance in the design of ship's service power systems on naval vessels. On shipboard the requirements for minimum weight, size, and complexity of electric equipment make it necessary to start relatively large motors across the line to avoid the use of reduced-voltage starting equipment as much as possible. For this reason and because of the necessity of main-

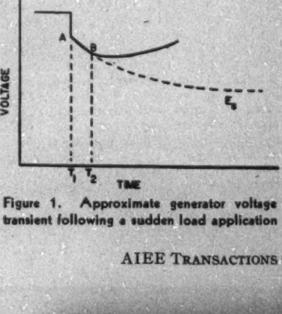


Figure 1. Approximate generator voltage transient following a sudden load application

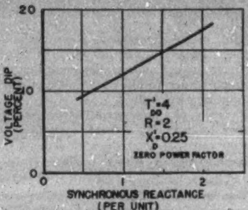


Figure 2. Voltage drop as function of synchronous reactance for an applied load of 50 per cent

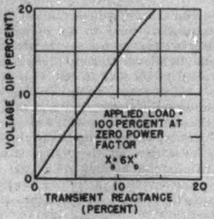


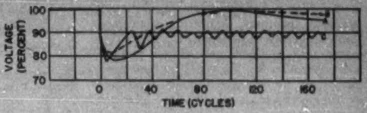
Figure 3. Effect of simultaneous change in transient and synchronous reactances on voltage drop

taining close tolerances on the system voltage disturbances, the generator voltage regulation is an important factor in the system design, and definite limitations always have been specified on the transient reactance of a-c generators built for naval service.

The analysis of the problem presented in the subject paper by Mr. Harder and Mr. Cheek is particularly valuable from the standpoint of showing the relative importance, not only of the various generator constants but also of the exciter and regulator characteristics in effecting the voltage drop on suddenly applied loads. It is desired, however, to point out a few additional considerations which have been encountered in connection with this problem on ship's service power systems.

On the low-voltage (600 volts or less) a-c systems which customarily are used on shipboard, the short-circuit currents and circuit-breaker interrupting ratings may become excessive unless an effort is made to keep the subtransient reactance high on relatively large-capacity installations. However, since the subtransient and transient reactances of a generator are not independent of each other, a compromise must be made between the low-reactance values dictated by voltage-drop requirements and the high-reactance values necessary to reduce the short-circuit currents. Figure 1 of the authors' paper presents data which greatly facilitate the proper choice of these reactances. In general, for systems where the short-circuit currents are a major factor in determining circuit-breaker sizes, the required value of the subtransient reactance will restrict greatly the choice of the transient reactance, and in such cases an accurate method of determining the effect of the latter in the voltage drop is essential.

Figure 4. Voltage transients found on same generator under same load application and with regulators of widely different characteristics



Under some conditions it is worth while to list synchronous reactance among the generator constants materially affecting voltage dip. Figure 1 of this discussion shows approximately the generator voltage transient following a sudden load application. It consists of two parts:

1. An almost instantaneous voltage dip to *a*, caused by generator transient reactance.

2. A gradual voltage dip from *a* (time t_1) to *b* (time t_2) when the regulator acts.

The change in voltage during the latter period, for the same regulator delay, is dependent on the slope of the voltage curve during the interval, and this in turn is determined by the open-circuit transient time constant and synchronous reactance. Synchronous reactance determines the steady-state voltage, e_s, toward which the voltage decays, and the open-circuit transient time constant determines the time it takes to reach any given portion of that value. It would seem, therefore, that synchronous reactance in some cases would have an effect equally as important as open-circuit transient time constant.

In general, the effect of synchronous reactance on voltage dip increases with decreasing applied load and with decreases in transient reactance. For example, by using the methods described by the authors to determine the voltage drop when the synchronous reactance differs from 120 per cent, Figure 2 of this discussion has been derived, showing the voltage drop as a function of the synchronous reactance for an applied load of 50 per cent. This figure shows that x_d has a much greater effect on the voltage drop than is indicated by the authors in their Figure 6b wherein the applied load was 100 per cent.

In addition, it should be noted that, when an attempt is made to reduce the transient reactance of a generator of a given rating, it may become necessary to decrease the armature leakage reactance. This will result in an increase in the physical size of the machine and a decrease in the synchronous reactance. The effect of this simultaneous change in the transient and synchronous reactances on voltage drop is shown in Figure 3 of this discussion. In those cases, therefore, where an increase in the weight and space requirements of the generator can be tolerated, a substantial reduction in voltage drop can be secured by decreasing the transient reactance to quite low values. However, as pointed out previously, the subtransient reactance will also be very low, resulting in high short-circuit currents.

In taking into account these factors in connection with the design of ship's service power systems, it has been found that a generator transient reactance of approximately 20 per cent in general is satisfactory from the standpoint of both system performance and machine size and weight.

The Navy has carried out an extensive investigation of the effect of regulator and exciter action on the magnitude of generator voltage dips with suddenly applied loads. Figure 4 of this discussion shows the voltage transients which were found on the same generator under the same load application and with regulators of widely different characteristics. It is evident that despite the differences in regulators the change in the magnitude of the maximum voltage drop is not very great. It should be noted, however, that the duration of the period of low voltage is quite different for the three regulators. Whether or not these differences in recovery time are important depends upon the operating characteristics of the system. In general, there is little advantage between the performance shown by curves *a* and *b*, whereas that of curve *c* is considered somewhat inferior for naval service because of the excessively long recovery time and rapid oscillations in voltage.

Frank V. Smith (Sargent and Lundy, Chicago, Ill.): The authors have made a valuable analysis of the factors affecting voltage drop and have put these together in a usable form. A study of this kind has long been needed and should be very helpful in shortening the work involved in many applications. One factor should receive greater emphasis. The curves in the paper are based on *self-excited* exciters. If the exciter is *separately* excited the voltage drop may be appreciably less than indicated. Complete calculations were made in accordance with the method of Wagner given in reference 1 of the paper for the following conditions:

Generator rated 5,200 kva, 0.8 power factor
6,300 volts, three-phase, 50-cycle 3,000 rpm
$X_d'=0.16$, $X_d=1.20$, $X_q=1.15$,
$X_p=0.11$, $r=0.005$, $T_{do}'=3.8$
Nominal response $R=0.7$
Regulator time = 0.05 second
Excitation required for saturation at no load, normal voltage 21 per cent
Suddenly applied load: 5,000 kva, 0.35 power factor constant impedance

For these conditions the maximum voltage drop following no load was 15 per cent and following full load 13 per cent. Figure 1 of the paper would give 22 per cent. The difference lies primarily in the fact that the response curve of the separately excited exciter is of the form $(1-\epsilon^{-t/T_e})$ rather than (ϵ^{-t/T_e}) used in the paper. It would seem that, where conditions warrant the use of a separately excited exciter, they will probably justify carrying out the complete calculation.

E. L. Harder and R. C. Cheek: Captain Rickover has emphasized the effect of synchronous reactance on the voltage dip. Synchronous reactance is without doubt the next most important factor beyond those used in Figure 1 of the paper (applied load, transient reactance, generator time constant, and exciter response). It was for this reason that an exact interpolation method was given whereby dip could be read from the curves for any synchronous reactance by using corrected values of added load and transient reactance.

In normally designed machines the time constant, T_{d0}', varies with the machine size as shown in Figure 4 of the paper. The synchronous reactance X_d, has a normal value slightly over unity, irrespective of size, with departures on either side as shown in Table I of the paper. Thus variations in T_{d0}' must be considered for average design machines of different size, whereas synchronous reactance need be considered only for considerable departures from normal design.

Nevertheless, as Captain Rickover points out, in some cases synchronous reactance would be equally as important as open-circuit time constant. This is true particularly when different machine designs are considered for the same rating.

It is interesting to note the empirical relation that the maximum voltage dip was nearly the same for three widely different regulating systems in the tests shown in Figure 4 of the discussion. This is due to the fact that all three systems were comparatively fast. In each case the drop was only moderately in excess of the initial transient drop. Had the exciter–regulator system been slow compared with the generator time constant, much larger differences might have been noted.

The discussion by Captain Rickover is a valuable and interesting analysis of the special considerations entering into the voltage-dip problem on naval vessels.

Frank Smith cites a case calculated for a separately excited exciter, in which the voltage dip was much less than would be obtained by using the curves of Figure 1 of the paper. The curves of the paper were calculated for and apply specifically to the case of the self-excited exciter. However, as pointed out in the paper, these curves may be used as an approximation in the case of separately excited exciters. If this is done, the results invariably will be pessimistic, although, as the exciter response is increased and the generator time constant lessened as compared with the case calculated by Mr. Smith, the approximation will be closer.

The Effect of Kilovar Supply on the Design of Systems for Load Growth

Discussion and authors' closure of paper 44-23 by T. W. Schroeder, J. W. Butler, and N. H. Meyers, presented at the AIEE winter technical meeting, New York, N. Y., January 24–28, 1944, and published in AIEE TRANSACTIONS, 1944, February section, pages 69–77.

B. M. Jones (Duquesne Light Company, Pittsburgh, Pa.): I think that the working out of typical cases, as the authors did, is very helpful on any technical and scientific problem. It is all very well to work out the theory and the high-powered scientific solutions, but the men who are going to use them want to see typical examples worked out also, and I believe that that is a very good point in this paper.

Table I. Feeder Loads—Present

Circled Number	Type	Kilowatts	Kilovars	Power Factor	Duquesne Light Co. Power Factor
1	Residential	1,000	483	.90	.91*
2	Commercial	1,000	750	.80	
3	Industrial	1,000	882	.75	
4	Commercial network	15–1,000	750	.80	.93†
5*A*	13.8-kv industrial, 33-kv line	13,000	11,500	.75	.93.5‡
5*B*	13.8-kv industrial, 13.8-kv line	13,000	11,500	.75	
6	33-kv industrial, 33-kv line	14,000	12,400	.75	

Pittsburgh Railways at 11 and 22 kv—55,000 kilowatts at 100 per cent in 1942.

*Corresponding Duquesne Light Company distribution circuits are four-kilovolt three-phase four-wire circuits serving residential, commercial, and industrial combined, or industrial alone, and varying in size from a few hundred to 2,000 kw, there being some 200 circuits.

†Corresponding is the downtown low-voltage a-c network three-phase four-wire 115/199-volt system of approximately 28,000-kva load in 1942, which is supplied by eight 11-kv feeders.

‡Corresponding Duquesne Light Company load is supplied at 11 and 22 kv, and this 93.5 per cent power factor is on the basis of about 175 customers, some of which purchase the power at the incoming voltages, but in most cases there is transformation to various secondary voltages.

The justification for the installation of kilovar capacity will become harder and harder as the power factor of the system and/or of its component parts becomes higher. To state it another way, the target becomes smaller and smaller, and hence it is much harder to hit. The authors point this out very carefully.

Basically, of course, we all know that the proper location for corrective equipment, kilovar capacity, is at the terminals of the utilization device which produces power factor below unity. When this is done, all of the beneficial effects are reflected over the entire system from that point to and including the generators, whether they be voltage improvement, making available greater capacity, less heating, reduction in losses, and stability improvements in some cases.

With an existing system it is not always the economical thing to do, in solving a problem in a certain part of the system, to provide the kilovar capacity at the point of utilization, or as near thereto as is practicable, which means capacitors scattered around over the system. It may easily be that the solution is to provide a large block of kilovar capacity on the bus of the stepdown substation on the subtransmission feeders. Careful working out of the comparative economies in each case is the means of finding the proper solution, as the authors point out.

The rates of our system, the Duquesne Light Company, provide an incentive to the customers to install the kilovar capacity to improve their power factor, and since about 1928 or 1929 the power factor of 174 medium- and large-demand customers supplied at subtransmission voltages of 11 and

Table II. Typical Loads and Power Factors on Duquesne Light System for 1942

	December 17, 1942—7 P.M.		At Time of December '42 Station Peak	
	Megawatts	Power Factor	Megawatts	Power Factor
Power Stations				
Colfax	250	82	257	81.8
Reed	191	91.4	191	92.0
Phillips	43	76.4	59	83.9
Brunot Island	66	90	56	81.6
Stanwix	5	100	5	100
Total	555 at	86.7	568 at	86.2
Major Supply Points to 22 Kv System				
Ambridge	44.0	97.0	44.0	92.4
Brunot Island	125.5	91.2	128.9	88.6
Dravosburg	52.0	94.8	54.0	91.2
Highland	40.7	93.0	48.6	92.1
North	30.0	88.9	33.0	90.6
Phillips	19.0	99.0	20.0	94.4
Pine Creek	42.0	93.3	46.0	91.5
Valley	30.0	94.0	30.8	91.3
Wilmerding	39.1	96.0	44.0	94.5
Woodville	44.5	98.4	49.5	97.8
Average	—	94.6	—	92.4
Customer A–66 kv	28.6		38.9	100
Customer B–66 kv	21.1		24.2	93.5
Customer C–66 kv	12.4		23.6	78.1
Customer D–66 kv	5.0		8.6	84.0
Distribution Substation—4 Kv, Larger Sizes				
Allegheny	9.6	87.1	10.0	88.1
Bellevue	6.3	90.2	7.5	91.3
Grant	11.3	90.5	15.8	90.5
Lawrence	7.2	95.2	9.0	91.2
McKeesport	9.3	88.7	10.7	89.5
Schenley	13.1	92.0	13.9	92.0
Stanwix	4.3	93.2	7.6	93.9
Wilkinsburg	9.6	90.0	10.4	89.1
Wilmerding	5.9	90.2	6.3	90.6
Average	—	90.8	—	90.7

22 kilovolts has increased from an average of 88 to 93.5 per cent in 1942, and the simultaneous load of these customers was approximately 270,000 kilowatts. To produce this improvement which, of course, was justified in the rate structure, 44,000-kilovars capacity was required.

In Table I of the discussion I have shown the power factor for the table in the subcaption to Figure 1 of the paper, and have also shown the power factor for corresponding parts of the Duquesne Light Company system, which is materially higher.

I would like to point out an interesting item here that some of our customers supplied from the distribution system, who have a small demand (a few hundred kilowatts), also have provided corrective equipment because of the rate structures, but, of course, not in the magnitude as do those customers supplied at transmission and subtransmission voltages.

Typical loads and power factors on the Duquesne Light Company system, including those for the system peak of 555,000 kilowatts at 86.7 per cent at seven p.m. on December 17, 1942, are included in Table II of the discussion.

R. G. Hooke (Public Service Electric and Gas Company, Newark, N. J.): The paper by Schroeder, Butler, and Meyers seems to imply that new reactive loads should always be served by installation of static capacitors. By inference, therefore, new generators should have only enough reactive capacity to maintain stability, that is, they may be designed for 90 or 95 per cent power-factor operation at full rated kilowatt output. Capacitors, located near to the load, would be provided to carry the reactive demands of the system.

But is this actually good economy? Is it actually proved by the paper in question? These authors have started with a system load at the generator of 174 megawatts and assumed an increase in demand that results in a new load at the generators of from 280 to 290 megawatts, depending on the method of supply. They find that if capacitors are used very liberally, the necessary expansion can be accomplished with a saving of some $260,000 in a total incremental investment of some $2,500,000.

In our experience, this type of analysis frequently results in erroneous conclusions. The criterion of economy is the over-all cumulative annual carrying charge on facilities. To compare, from an economy standpoint, two methods of doing a job, we always look at the differential in cumulative carrying charges.

Electric-load growth is a gradual process. We do not have the ideal opportunity set forth in this study. Many of us, faced by a 60 per cent load increase distributed uniformly over our system would enjoy the opportunity to accomplish wholesale improvements—improvements which we cannot justify when faced with the actuality of gradual, intermittent, and geographically-irregular load increases. The paper would be more convincing if the load growth had been assumed to come on over a period of perhaps ten to 15 years, and if each increment of investment required had been determined for the two schemes under consideration, with annual carrying charges tabulated and cumulated. It may be that heavy investment in the "without capacitor" program would not be required until near the end of the period, so that cumulative annual costs, up to the time when the 60 per cent increase in load has been realized, would give an answer just the reverse of that indicated.

After all, of the $260,000 differential in favor of the capacitor plan, $212,000 is in the cost of a 25,000-kva synchronous condenser. Whether this particular item is required early or late in the expansion program may be a controlling factor in the economic answer.

Incidentally, it may be noted that this $260,000 differential is only about ten per cent of the total capital outlay involved—perhaps not within the accuracy of the estimates of various costs.

If the four hydro units serving substation *A* had happened to be located very near to the substation, as they might well be in another type of system, instead of 150 miles away, very probably no condenser would have been required; it would almost certainly have been more economical to carry the reactive on the generators themselves, and they should not in that case have been designed as unity power factor machines.

It is noted that, in this hypothetical problem, 91,000 kilovars of static capacitors are assumed installed on a system with maximum demand of 290,000 kilowatts and 250,000 kilovars. We assume devices have been evolved to remove some of these capacitors from the system during periods of light load; otherwise it appears that the generators would be operating at leading power factors, which is not generally accepted as reliable practice. We are interested in the question of automatic switching facilities for capacitors on distribution lines and would like to hear more about it.

Perhaps it will be pertinent for me to state briefly the conclusions we have come to on the general problem treated by this paper—the problem of economical design of a large growing system wherein the provision of adequate reactive capacity, with the highest possible over-all economy, has received considerable study.

With a demand of 1,000,000 kilowatts and some 875,000 kilovars at the generators, we have found that 135,000 kilovars in static capacitors on distribution lines and some 225,000 kilovars in synchronous condensers gave what we believed to be an economic balance. Allowing for some underloading of condensers to preserve ample spare ratios for voltage control and other operating requirements around the system, our generators at peak times may provide well over 500,000 kilovars of our reactive capacity. This means an average power factor of perhaps 88 per cent, with some machines operating higher, some substantially lower.

It is the opinion of our system planners that it will be economical always for us to use generators in this way, and we hope always to be able to purchase 80 per cent power-factor machines. We arrive at this conclusion after full consideration of savings possible in distribution, transformers and subtransmission through use of capacitors at or near the load. The explanation lies in the inherent overbuild of any system. At any particular period supply facilities to a particular area (call it area *A*) may be fully loaded; immediate growth will be met by installing capacitors until no more benefit is obtainable thereby. The next step is to provide a new line or transformer. Thereafter, for a time, there will be a substantial surplus or overbuild in this area *A*. Meanwhile, area *B* is becoming pressed for capacity, so we move the capacitors from area *A*, where they are no longer needed, to area *B* where again they can effect postponement of costly facilities. The reactive which they formerly supplied in area *A* can readily be carried back through the newly reinforced transformers and transmission to the generators, displacing there the reactive of area *B* formerly carried.

By this process very great savings in new facilities have been and can be achieved. Shifting capacitors from one "tight" area to another is a regular part of our program. Obviously it is important that the generators have sufficient kilovolt-ampere capacity to carry the reactive of those areas wherein the inevitable periodic overbuild of facilities results in there being practically no cost for the transmission of this load back to the station.

We do not, therefore, contemplate carrying all future incremental reactive loads on static capacitors, and we do not want generators designed for 90 or 95 per cent power factor, particularly if the turbines should be built with overload capacity and the generators without.

J. G. Holm (Boston Port of Embarkation, Boston, Mass.): The authors have presented a paper on a very important subject. The paper discloses a vast cumulative store of knowledge and involves the many phases inherent to the problem discussed. That shunt capacitors in power systems in numerous cases prove to be economically advantageous should not be doubted. That the authors proved this convincingly on the system chosen is not so obvious.

When the economic advantage of a certain system is only ten per cent in favor of it, it lies within the figures of permissible range of engineering accuracy. Since the latter is generally recognized to be within legitimate limits when it does not exceed ten to 15 per cent, one hesitates to accept unequivocally the ten per cent figure given as the economic advantage in favor of capacitors. A ten per cent error may easily nullify the ten per cent gain obtained.

If one would like to follow the authors' figures for one's own satisfaction, as well as in order to apply the results to a particular case, the attempt will meet with difficulties. The authors gave only capital-cost figures, but did not indicate how the particular cost items were arrived at from their component parts. For example, the cost of capacitors is given after they are added to the system, and no indication is given as to what portion of this cost is material cost, what is the cost of labor and installation, and what, if any, are the indirect expenses. When comparing with the cost of other items after they are added, such as transformers, or cable, one is bound to ask at least what is the basic price of capacitors, so that the difference between the cost as added and the cost of material could be calculated and compared with that for other equipment for the case without capacitors. This is necessary if for no other reason than to dispell the generally prevalent view that the cost of capacitors (price paid to manufacturers) is too high.

The comparison between the two cases—with and without capacitors—also should be made on the basis of the annual costs. The authors say that installation of the capacitor postpones the investment until such a time when larger equipment can be used. It might be that, when the capacitors are removed to be replaced by permanent equipment, another place in the system where they can be utilized to advantage will not be found; in such a case their obsolescence factor would become very high. This can be reflected and accounted for only in the annual costs, if these are calculated. The losses obviously will be different in the cases with and without capacitors; they should be accounted in the annual costs, and the cost of losses per unit should be given. As a result, it might be that the economic balance in favor of capacitors will be somewhat greater than the figure obtained by the authors, and at any rate, it will decide the case in a fashion leaving no doubts.

Some of the purely technical points of the basis of comparison of the two systems also should be clarified. The authors say that residential and commercial loads favor the capacitors to a greater extent than the industrial load. In such a case the future growth of the former loads should not have been assumed at 80 per cent and that of the latter at 50 per cent, even though this might not have represented an actual case correctly, or else, in the example taken for the study, the present industrial load should have been increased.

The authors quite properly state that economy favors capacitors on an over-all system basis. It seems to me that had they broadened their comparison still further by taking into account the incremental generating capacity in the two cases considered, had they been more specific with the margins for future load growth, and consequently, had they accounted for the spare generating capacity, the economic advantage in favor of capacitors well could be greater.

There are unquestionably very many possibilities and many factors involved in this broad problem, such as, for instance, the economic feasibility of the use of reactive power supplied by the receiving-end metropolitan centers located at the end of long, or short, feeders. Possibly there are too many factors to permit for a lucid presentation in one paper. The authors instead have chosen a rather complicated example in order to prove their point. This fact, as well as the several phases of the problem discussed left to the reader for evaluation, leave the subject not proved convincingly. This I say with all regard to the good work the authors have been doing.

T. W. Schroeder, J. W. Butler, and **N. H. Meyers:** It was not the authors' purpose to evaluate specifically the saving in installed equipment cost to be expected by the use of capacitors. To determine a dollar figure of this kind for a synthetic system, and to attempt to extrapolate it to apply to power systems generally, would be throwing oneself to the mercy of many more justifiable arguments regarding the effect of variations of character of different portions of the system from those advanced by the discussers of the paper.

Rather, the objective was to combine the thought that on certain feeders alone the capacitor proves economical with the thought that the greatest use in system investment may be obtained by supplying the kilovars near the load in the application of capacitors on a *system-wide* basis. The importance of an *over-all complete-system* viewpoint was stressed, as compared with a viewpoint concerning itself with a single feeder or any other single item at a time.

A soundly designed system was chosen, in which heavily loaded low-voltage circuits were short, by virtue of the use throughout of the small- or unit-substation method of design. Such a system will be obviously less favorable to the use of capacitors. It was recognized that in many existing systems not so fortunately endowed, the use of capacitors would result in larger gains, and even had the ten per cent gain obtained in the case chosen turned out to be minus ten per cent, the goal of realizing the importance of the over-all viewpoint would not have been lost; furthermore, there were operating advantages and loss reductions not evaluated in dollars.

Also, not evaluated was the advantage of the use of capacitors in postponing the need for the inevitable overbuild of a part of a system as discussed by Mr. Hooke—the role in which the capacitor truly becomes a tool or steppingstone in system design. It is now not uncommon in system planning to project future kilovar loads and to determine from whence those will be supplied, even as has been the practice for years with the kilowatt requirements.

Mr. Jones' actual system data gives an interesting comparison with the system studied, showing load and generator power factors comparable with those obtaining for the "future load with capacitor" case reported in the paper. That the reason for this in the actual case is because the rate structure is conducive to the utility's customers installing the capacitors is incidental; it was not necessary for the authors to solve the ticklish problem of whether the utility or its customers would supply the kilovars. But in most cases, the location of the capacitors on the system studied would be substantially the same regardless of this.

Mr. Hooke's feeling that the authors advocate the supply of all future-load kilovars by capacitors and the resultant purchase of future generators for 95–100 per cent power factor is not borne out by the result of the study. The "future load, with capacitor" combination resulted in generator loadings at around 90 per cent power factor, and it would be expected that when further load growth is experienced the overbuild of system will demand that the generators supply the new kilovars for a time, with a resultant lowering of the power factor, or, at the outside, with no change in power factor.

In view of the objective aimed for, as discussed above, it was not necessary to go into the complications of cumulative annual carrying charges as stressed by Mr. Hooke, or the obsolescence factor as mentioned by Mr. Holm. However, although Mr. Hooke points out that the heavy investment in the "without capacitor" program would not be required until the end of a growth period, it cannot be denied that the capacitor program would have delayed the heavy investment, thus extending the growth period and adding to gains already indicated.

Mr. Hooke outlines a process of providing for load growth with the aid of capacitors in complete agreement with the steppingstone concept discussed in the paper. His assumption that devices to remove automatically part of the capacitors at light load were included in the costs is correct. Such devices have been available in the past, and they will be available in improved form and at reduced cost in the future.

Mr. Holm's comment that the authors might have accounted for the spare generating capacity to the advantage of the capacitor is well taken. Neglecting to do this was in accordance with the authors' plan to be conservative. In answer to the question concerning costs, it should be pointed out that these were obtained from published handbook data for equipment cost plus what was considered a reasonable addition for installation.

Inverter Action on Reversing of Thyratron-Motor Control

Discussion and authors' closure of paper 44-18 by H. L. Palmer and H. H. Leigh, presented at the AIEE winter technical meeting, New York, N. Y., January 24–28, 1944, and published in AIEE TRANSACTIONS, 1944, April section, pages 175–84.

Victor Siegfried (Worcester Polytechnic Institute, Worcester, Mass.): The authors' description of the inverter action of the thyratron-motor control upon reversal has added considerably to the art of supplying d-c motors from rectifiers. The application of this feature has extended the usefulness of the drive to include practically every feature previously available with conventional generator supplies. The material in this paper also brings out clearly a number of the relations which occur in the rectifier-supplied motor whether reversing or nonreversing.

From the oscillograms in the paper, it is to be seen that the shape of the current wave is considerably different from the smooth continuous wave usually expected in d-c motors. Also, the voltage wave includes instantaneous peaks far in excess of the average voltage associated with the nameplate voltage. These give rise to further effects in the motors which deserve considerable attention before the Institute if applications of this drive are to be engineered properly. These are:

1. The rating of the motor must be adjusted downward to take account of the greater heating caused in it by currents of these shapes.
2. Insulation of the armature coils and of the commutator must be increased in order to be consistent with the peak voltages encountered, to achieve expected insulation life.
3. Torque pulsations must be smoothed out by mechanical means or by use of enough phases, to eliminate vibration and noise.

J. W. Picking (Reliance Electric and Engineering Company, Cleveland, Ohio): The material presented in this paper covers a very interesting application of electronic control to industrial electric-motor drives. The circuits developed are ingenious and apparently well tried and proved. Evidently a problem was presented, and an almost ideal set of conditions have been met.

It is a good practice to take an inventory of the conventional versus the new at this point, and also to check any possible improvements to the conventional systems. Let us check these points at this time.

To obtain reversing when voltage control only is used, the inversion system apparently adds to the nonreversing drive four contactor interlocks, six resistors, five capacitors, and a relay. The contactor system adds a dynamic-brake contact, resistor, and an antiplug relay.

If field-weakening control is added, inverter control adds, in addition to the aforementioned, a high-vacuum tube, three resistors, and three capacitors. For contactor systems a full field relay is required.

It is evident that inversion requires more parts and considerable circuit complications.

The advantages claimed for this system are a more rapid reversal and constant current throughout the cycle. It is assumed that speed of reversal is of paramount importance. In this case, it is desirable to take advantage of the fact that d-c motors can commutate very high currents (up to 400 per cent) at low voltages. Plugging can be introduced during deceleration, at speeds as high as 20 per cent of base speed, and the biggest disadvantage of dynamic braking is eliminated. If a more rapid deceleration is required, graduated dynamic braking can be added to reduce the effective circuit resistance as the braking current falls off.

For stopping only, the inversion system requires full reversing control including the reversing contactor and apparently adds in addition to the material previously listed two relays, a high-vacuum tube, three resistors, and a capacitor.

To approximate the decelerating time realized by the inversion system, at least two steps of graduated dynamic braking would be required when friction loads are encountered and three steps for inertia loads. At least for the present, thyratron rectifier drives are limited to the lower horsepowers. It should be entirely practicable to develop multiple-step graduated dynamic braking, which compares favorably with the added costs of the inversion components and would involve considerably less circuit complication.

There are undoubtedly certain applications where inversion control is the correct solution, but it should be used only where necessary if we are to overcome the opposition to electronic drives of many users in the field.

K. P. Puchlowski (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): It is a real pleasure to take the opportunity to congratulate the authors on their valuable and interesting paper which acquires additional significance in the light of the recent engineering progress and special trends toward the development and perfection of electronic motor drives.

However, a number of objections may be raised concerning the authors' approach to the problem of continuous current as well as commutation during inversion. It seems that the authors are emphasizing particularly the practical importance of continuous current during the inversion, and, consequently, they have devoted a major portion of their paper to the qualitative discussion of inversion under these conditions. Yet, a straightforward assumption made by the authors that a sufficiently high inductance is introduced in the circuit to give continuous conduction of current seems to be an oversimplification of the problem. A statement like that creates the impression that a sufficiently high inductance is all that is needed to secure continuous conduction, which in general is not the case.

A closer analysis of the problem will reveal that there are definite limitations of conditions for continuous conduction, and these limitations do not depend solely upon the magnitude of circuit inductance (strictly $\omega L/R$). During the inversion, similarly as during the rectification, the ratio of the motor-generated voltage to the transformer voltage plays a very important part. There exists a definite range of firing angles of the rectifier tubes where continuous conduction cannot be obtained, regardless of the value of circuit inductance, this range being determined by the previously mentioned voltage ratio.

The problem of proper commutation in the case of continuous conduction is fully recognized by the authors who imply the existence of a maximum firing-angle limit by stating that in order to obtain proper commutation during inversion the firing angle in a single-phase full-wave inverter (biphase inverter) operating with continuous current should be less than 180 degrees. However, this general statement does not represent the actual limitation of the firing angle which in fact is much more critical. As in the case of current continuity, the limitation of the firing angle from the point of view of satisfactory commutation is determined strictly by the relative magnitudes of the motor-generated and transformer voltages, a situation which does not seem to be fully recognized by the authors.

Since, as it is rightly stated in the paper, the problem of obtaining satisfactory commutation from tube to tube during the inversion exists only in the case of continuous current, it is apparent that the firing-angle limitations for continuous conduction must be tied up in some manner with those necessary to secure satisfactory commutation.

A strict analysis of the problem will reveal that this situation can be represented mathematically in the following manner:

If the voltage generated in the motor is denoted by E_g, the peak value of transformer voltage by $E_{\max}$ and the firing angle of the thyratron by x_f, two different cases must be considered:

$$A.\quad E_g < \frac{2E_{\max}}{\sqrt{4+\pi^2}} \tag{1}$$

Here, the necessary (though not the only) condition for continuous conduction is at the same time a sufficient condition for securing satisfactory commutation and can be represented by

$$x_f < \arccos\left(-\frac{\pi E_g}{2E_{\max}}\right) \tag{2}$$

$$B.\quad E_g > \frac{2E_{\max}}{\sqrt{4+\pi^2}} \tag{3}$$

and the necessary condition for satisfactory commutation

$$x_f < \arccos\left(-\sqrt{1-\frac{E_g^2}{E_{\max}^2}}\right) \tag{4}$$

also will represent the range of firing angles where continuous conduction can be obtained with proper value of circuit inductance.

It should be pointed out that case *A* is met with most often in practice. For example, referring to values of the motor-generated voltage and transformer voltage given in the paper, we have

$E_g = 230$ volts
$E_{\max} = 440$ volts (on the basis of 280 volts average value)

For these voltages the relation 1 is satisfied since

$$\frac{2E_{\max}}{\sqrt{4+\pi^2}} = 236 > E_g$$

Hence, from relation 2 we obtain the absolutely necessary condition which must be met, regardless of the value of inductance in order to obtain continuous conduction:

$$x_f < 145°18'$$

When, during inversion, the firing angle is delayed more than 145 degrees 18 minutes, the current always will be discontinuous, even with any additional reactor in series with the armature, and, therefore, the commutation problem will be nonexistent. If, on the other hand, the firing angle is less than 145 degrees 18 minutes, continuous conduction may be obtained if the inductance of the circuit is sufficiently high. If, however, continuous conduction is obtained, there will be no troubles with commutation since within that range the firing angle always will satisfy the condition necessary for proper commutation.

In the light of these considerations, it seems that the authors' reference to 180 degrees as a certain limiting value for firing angles, important to insure proper operation during inversion, is not justified since the angle of 180 degrees does not represent a value of any particular interest or significance.

Furthermore, it is apparent that for the values of voltages given by the authors, as well as for those met with in most practical cases, the firing angle during inversion may be delayed safely to 180 degrees or even more without any commutation troubles, since this will result simply in discontinuous current assuming the shape of discrete pulses.

H. L. Palmer and H. H. Leigh: We welcome the opportunity of acknowledging the contributions of Puchlowski, Picking, and Siegfried as adding to and clarifying this subject material. We also wish to confirm some points and disagree with other points which they have advanced.

May we first consider Mr. Puchlowski's discussion.

If the paper conveys to the reader any particular emphasis on the necessity or undue importance of continuous current during inversion, we wish to correct that impression. The case of sufficient inductance to give continuous conduction was chosen to first illustrate the principle of inversion as being most nearly comparable to the case of regeneration of a motor into a d-c generator, as compared in Figure 2 of the paper. As was brought out in the paper, inversion can and does take place under conditions of both continuous and discontinuous currents.

May we, in turn, be permitted to raise a few objections to Mr. Puchlowski's approach to the problem of continuous current and commutation during inversion. We cannot agree with his concept of the limitation of the firing angle for satisfactory commutation and the tie between this limiting angle and the firing-angle limitations for continuous conduction as we understand it from his discussion.

It is evident that unless there is continuous conduction there is no commutation of the current from tube to tube. However, the conditions of motor-generated voltage, transformer voltage, firing angle and circuit inductance which will give continuous conduction will not insure satisfactory commutation. To commutate satisfactorily the current from one tube to another, the firing angle must be retarded less than 180 degrees less the angle required to commutate the current, so that the commutation is completed before the anode voltage of the "offgoing" tube becomes more positive than the anode of the "incoming" tube. This angle required to commutate the current is a function of the commutating reactance of the transformer and the d-c output current. A study of Figure 3 of the paper may make this more clear. This limiting firing angle for satisfactory commutation has no direct relation to the firing angle required to give continuous conduction, and for a biphase half-wave circuit with the commutating reactance only in the two halves of the transformer secondary winding, it is of the form

$$X_f < \text{arc}\cos-\left(1-\frac{I_{dc}X_l}{E_{\max}}\right)$$

where X_l is the commutating reactance of the transformer secondary winding.

In Mr. Puchlowski's analysis of the requirements for continuous current he considers two cases, for each of which he gives a limiting firing angle as a necessary condition for continuous conduction. The limiting firing angle in case *A* appears to come from the well-known equation for the average output voltage of a phase-controlled rectifier operating at continuous conduction and neglecting regulation due to commutation

$$E_0=E_{\max}\frac{P}{\pi}\ \sin\frac{\pi}{P}\cos X_f$$

where P is the number of phases, $E_{\max}$ is the peak value of transformer voltage and X_F is the firing angle. Or for the biphase half-wave circuit:

$$E_0=\frac{2E_{\max}}{\pi}\cos X_f=E_g+IR$$

where E_g is the generated voltage of the motor, I the d-c armature current, and R the motor and circuit resistance. Then

$$\cos X_F=\frac{\pi(E_g+IR)}{2E_{\max}}$$

or

$$X_F=\text{arc}\cos\frac{\pi(E_g+IR)}{2E_{\max}}$$

is a necessary condition for continuous conduction. But with a highly inductive load circuit it is a necessary condition for any appreciable conduction at all. Since we are generally not interested in inverting at very low average motor current for the most part not less than rated motor current, it is not at all impractical to find sufficient reactance to give continuous conduction at the desired operating current. With sufficient inductance in the motor circuit, it is impossible to get discontinuous current or discrete pulses of current at the average value desired. Then the firing-angle limitation given is not so much a limit for continuous conduction as it is a limitation for the desired average current.

The aforementioned relation of E_g, $E_{\max}$, X_F, and I holds true between the limits.

$$\frac{2E_{\max}}{\pi}>E_g>-\frac{2E_{\max}}{\pi}$$

if the regulation due to commutation is neglected. We fail to see any particular significance to the limit set by Mr. Puchlowski of

$$E_g<\frac{2E_{\max}}{\sqrt{4+\pi^2}}$$

When $E_g>(2E_{\max}/\pi)$ the current will be discontinuous pulses with a low average value. When $-E_g$ is more negative than $-2E_{\max}/\pi$, the current will be continuous. Under this condition the firing angle would be retarded 180 degrees, and this would fail to give satisfactory commutation. Hence it is necessary to limit the value of $-E_g$ to something less than $-2E_{\max}/\pi$ so that it will not be necessary to retard the firing angle to 180 degrees less the commutation angle.

The limiting firing angle which Mr. Puchlowski gives for his case *B* indicates that, as $-E_g$ increases in magnitude, the firing angle must be advanced to maintain continuous conduction. Actually, just the opposite is true. As E_g is increased in the negative direction, the firing angle can be retarded and still maintain continuous conduction and must be retarded to limit the current to a reasonable value.

In the practical case a motor may have sufficient inductance in itself at weak field to give continuous conduction at any firing angle which will give the desired average current. Hence, it is impractical to invert under conditions which require the firing to be retarded 180 degrees less the angle required for commutation. This of course means that the negative E_g must be limited to less than $-2E_{\max}/\pi$ by limiting the building up of the field. Operating under this condition, the firing point will be in advance of the commutating limit under load, and if it is retarded the current will decrease and become discontinuous as in Mr. Puchlowski's example.

Mr. Picking's count of the additional parts required for reversing by inversion indicates that there are two points which apparently were not made clear in the paper and which we would like to clarify at this opportunity:

1. **The preconditioning circuit is generally used on nonreversing equipments exactly as used on the reversing and would be used if dynamic braking were utilized during reversing, to prevent closing the reverse or zero generated voltage of the motor into the full on rectifier voltage. Hence this circuit should not be considered as an addition required by inversion alone.**

2. **The three additional resistors and capacitors shown in the grid circuit of the armature thyratrons were presented as an alternative to the condition that preconditioning or operation never retard the firing a full 180 degrees during reversal, even under conditions of discontinuous current. Generally in the biphase circuit described here the control never retards the firing of the thyratrons near 180 degrees. Hence, altering the grid-voltage wave shape is not necessary. This reduces the total count of additional parts to two interlocks, four resistors, two capacitors, a vacuum tube, and a relay.**

It should be pointed out further that these parts are small and inexpensive, and that the circuit complications of inversion over dynamic braking, particularly where stepped dynamic braking is considered, is a matter of viewpoint.

One other consideration in a comparison of inversion and dynamic braking might be pointed out. Where reversal is frequent, the space and ventilation required for the dynamic-braking resistor to dissipate the energy of decelerating the motor may become an important consideration. With inversion this energy is not dissipated but is pumped back into the line.

For stopping only, the inversion system is somewhat more complicated than dynamic braking and, as Mr. Picking points out, should be used only where its improved characteristics will justify the additional equipment.

We wish to acknowledge Mr. Siegfried's discussion as pointing out at least three important factors in the design and application of electronic motor control. However, these factors are beyond the scope of this paper and indeed deserve more discussion than can be given here.

The Asymmetrical Stator as a Means of Starting Single-Phase Induction Motors

Discussion and author's closure of paper 44-35 by John L. Baum, presented at the AIEE winter technical meeting, New York, N. Y., January 24-28, 1944, and published in AIEE TRANSACTIONS, 1944, May section, pages 245-50.

E. E. Dreese (Ohio State University, Columbus, Ohio): It has been known for some time that by the use of asymmetry it is possible to obtain starting torque in a single-phase motor with a single winding. Such a motor is economically and technically desirable because of the simplicity in avoiding a separate starting winding and switch and, therefore, giving an attendant saving in manufacturing cost. Another advantage is the avoidance of maintenance troubles and outages, since by far the greatest single factor causing breakdowns in single-phase motors is trouble with the starting winding and its automatic switch.

Recent developments in single-winding

single-phase self-starting motors illustrate the American tradition of progress made by hard-working and ingenious individuals without the benefits of modern laboratories and shops. Two instances in this single-phase motor field illustrate this. The present author is a graduate in physics, but in later years has been operating a successful apple orchard in southern Ohio. I am told that when it became necessary for him to find the speed of a motor operating subsynchronously he resorted to the device of mounting a gear wheel on the shaft of the motor and allowing the gear teeth to produce a note by displacing a piece of flexible steel and beating the resultant note against notes from a harmonical The other instance is a Mr. Earl Oswald, who is working in the field of single-winding single-phase motors. Mr. Oswald has produced such a motor with commercial possibilities. He too has worked under difficulties in a shop in his garage.

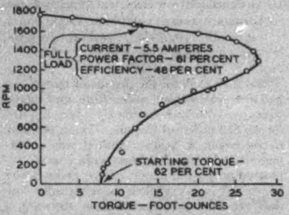


Figure 1. Speed–torque graph showing the performance of one of Oswald's motors

$^1/_4$ horsepower; 110 volts; single phase, four poles

The speed–torque graph in Figure 1 shows the performance of one of Oswald's motors. It will be noted that the starting torque is 62 per cent of full-load torque. The full-load current is 5.5 amperes, and the full load efficiency and power factor are 48 per cent and 61 per cent, respectively. Later motors have practically 100 per cent starting torque.

The "bete noire" of such motors as described by Mr. Baum has been the disturbing effect of the third harmonic, which in extreme cases makes the motor run subsynchronously even at no load. This is illustrated by the aforementioned harmonica incident. In the Oswald curve shown, this third-harmonic difficulty has been overcome. A plotting of the curve *through* actual points of the speed–torque curve shown does indicate a "vestigial remnant" in the neighborhood of 600 rpm, but the remnant is of minor consequence since it is small and not re-entrant.

John L. Baum (nonmember): The author wishes to thank Mr. Dreese for the information concerning the Oswald motor, for it was hearing indirectly of this motor that called his attention to the problem. This motor has more turns about one side of the pole than the other, producing unequal saturation. Since this saturation is in part the result of both stator and rotor leakage flux, accompanying the starting current, under lower full-load currents the saturation is reduced, and normal running conditions are made possible. The author hopes to investigate this type of motor more thoroughly.

The varying-air-gap motor has been manufactured by the Bodine Electric Company of Chicago.

This paper suggests that with the varying-air-gap motor the difference in permeability resulting from higher flux densities where the air gap is lower will not be serious. But further study and experiment indicates that this is not always the case. In an experimental motor, originally a common type of $^1/_4$-horsepower four-pole split-phase motor, the measured starting torque is only 38 per cent of the calculated value. Upon checking flux densities, it is found that whereas the rotor is not appreciably saturated, the portion of the rotor leakage flux which passes through the stator teeth, together with the stator leakage flux, saturates the stator-tooth tips. This tends to increase the effective air gap, and to a greater extent where it is shorter and the zigzag flux greater. This in turn reduces the difference between the properties of the two sides of the pole, hence the torque. The trouble might be helped by using thick stator-tooth tips.

A Design Method for D-C Machines

Discussion of paper 44-63 by M. I. Beers and J. H. Kerr, presented at the AIEE winter technical meeting, New York, N. Y., January 24–28, 1944, and published in AIEE TRANSACTIONS, 1944, April section, pages 172–5.

C. W. Lange (nonmember; General Electric Company, Erie, Pa.): This paper describes a method and includes certain formulations to aid in determining the armature and field windings for different ratings in small-size d-c machines already developed for other ratings.

The proposed method uses well-known principles and relationships and could be useful in connection with electrical modifications of small d-c machines of the type considered, having a relatively small and fixed number of armature slots and commutator bars and a relatively large number of armature turns per coil, but the method would not be wholly adequate for larger machines for which more design factors must be considered and greater refinement of design is required.

The method appears more elaborate than necessary for the purpose, or than the methods generally employed.

The method does not take into consideration the variation in space factor with different windings, and results obtained would, therefore, be in the nature of first approximations.

After the design of a small d-c machine for a given rating has been completed, certain values of flux per pole for series-, compound-, and shunt-wound machines become established, and such flux values afford a general indication of the size of field windings which can be used in such a machine, the values being modified within certain limits to suit the speed at which the machine is to operate and the closeness with which the trial armature winding approximates the required total number of turns.

The conventional equation for flux per pole can be used then to determine the number of turns or conductors in the armature winding corresponding to such values of flux.

Attention is directed in the paper to the fact that for a given cross section of winding and mean length of turn, the resistance at a specified temperature of any winding having such dimensions is approximately proportional to the square of the number of turns.

This known relationship is of use in designing special windings, whether for the armature, the commutating field, or the main field, for any d-c machine already designed, due consideration being given to the variation in space factor for different windings.

This relationship can be very simply demonstrated in the following manner.

The basic expression for the resistance of a circuit at a specified temperature is:

$$r_\theta = k\rho_\theta \frac{l}{a} \tag{1}$$

where

r_θ = resistance of the circuit at a specified temperature θ

ρ_θ = resistance per inch length per square-inch section of copper conductor at a specified temperature θ

(At 25 degrees centigrade, $\rho_\theta = 0.6935 \times 10^{-6}$)

a = cross-sectional area of conductor, in square inches

l = total length of conductor, in inches $= MLT \times t$

MLT = mean length of turn in inches

t = total number of turns

k = a constant, dependent on the number of circuits and method of connection

For given values of MLT, k and ρ_θ, the expression 1 can be reduced to the following form:

$$r_\theta = k_1 \frac{t}{a} \tag{2}$$

where

k_1 = a constant involving k, ρ_θ, and MLT

Considering a given total cross section A for the copper, and space factor f_s,

$$f_sA = ta$$

$$a = f_s \frac{A}{t} \tag{3}$$

Substituting in equation 2 the value for a shown by equation 3,

$$r_\theta = k_1 t + f_s \frac{A}{t} = \frac{k_1}{f_sA} \times t^2 \tag{4}$$

Some Aspects of Electric-MotorDesign—Polyphase-Induction-Motor Design to Meet Fixed Specifications

Discussion of paper 44-2 by T. C. Lloyd, presented at the AIEE winter technical meeting, New York, N. Y., January 24–28, 1944, and published in AIEE TRANSACTIONS, 1944, January section, pages 14–20.

P. H. Trickey (Diehl Manufacturing Company, Elizabethport, N. J.): It is not to be wondered at, that Mr. Lloyd became discouraged in attempting to use the classical textbook formula for the D^2L of an induction motor when he found such variations in the so-called fundamental factors. Perhaps one of the reasons for the discrepancies, is that as far as I was taught by the older engineers, and as far as I have been able to determine myself, the horsepower of an induction motor is a function of the $(OD)^2W$ where OD is the diameter of the stator lamination, and W the core stack and not of the D^2L of the rotor. I have personally found that I could vary the rotor diameter and proportions of iron and copper through quite wide ranges with only small effect on the performance, but that I always had to have the same $(OD)^2W$ to equal a competitor.

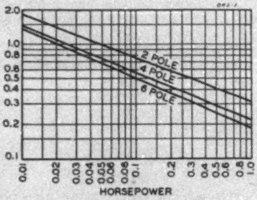


Figure 1. Fractional-horsepower-induction-motor size, C_1 versus horsepower

$$(OD)^2W = C_1 \; \frac{hp}{rpm} \; C_fC_t \times 10_6 \times \text{in.}^3$$

	C_f
60	1.00
50	0.96
25	0.87

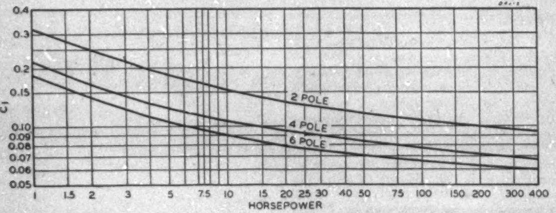


Figure 2. Integral-horsepower-induction-motor size, C_1 versus horsepower

OD, f, and C_f as in Figure 1

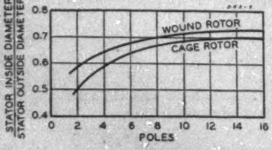


Figure 3. Inside diameter of stator

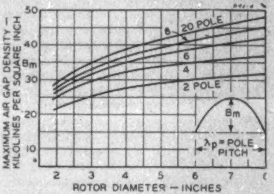


Figure 4. C. K. Hooper's curve of air-gap density of polyphase induction motors based on four pole laminations for all poles

$$B_m = \frac{E_{ph}\,45\times10^6}{CK_wf} \times \frac{1}{\lambda_p W} \times \frac{\pi}{2}$$

E_{ph} = volts per phase
CK_w = effective series conductors per phase
f = frequency
W = axial core stack
Multiply B_m by
1.2 for 25 cycles
1.05 for 50 cycles
0.9 for fan duty
1.10 for single-phase general purpose
1.32 for single-phase special service

Table I. Type Constants for Use With Figures 1 and 2

C_T	Remarks
1.00	Polyphase general purpose
1.42	Split-phase general purpose
1.25	Split-phase special-service machine wound
1.09	Split-phase special-service hand wound
1.25	Capacitor-start special service
1.42	Capacitor-start general purpose
1.42	Repulsion-start special service
1.85	Repulsion-start general purpose

Some years ago, H. E. Ellis of the Westinghouse Company and I developed a method of determining the size of an induction motor which has never been published, but perhaps it would be worth while to mention it here in case someone might wish to use it. We were developing a complete line of fractional-horsepower motors at the time, and each rating was very carefully designed with complete reference to sizes, and performance of many competitive motors, and with very thorough investigation of copper and iron proportions as well as winding tightness and costs. After we had many ratings designed, we decided there must be some way to get out first trial on core stack more nearly correct, so we "made up" a formula which we thought must represent motor volume

$$(OD)^2W = C_1\frac{hp}{rpm} \quad C_fC_T\times10^8 \qquad (1)$$

where

C_1 is dependent on horsepower and poles.
C_f is dependent on frequency.
C_T is dependent on electrical type and performance such as polyphase, single phase, capacitor, repulsion, general purpose, special service, and so on.

We then took all the dozens of motors we had already designed so carefully, and solved equation 1 backwards for C_1. We did this for all the different groups. Upon plotting C_1 we began to suspect that it was an exponential function of horsepower, so we replotted on log-log paper. Sure enough the points lay so very close to a straight line that we felt quite justified in drawing a straight line through them, although later we modified this as shown in Figure 2.

The charts shown in Figures 1 and 2 of this discussion are the result, and Table I shows some typical values for C_T. We thought at first that C_f would vary with frequency, but now suspect that it may possibly be 1.00 for all frequencies. Having chosen the stator bore from Figure 3 and the air-gap density from C. K. Hooper's curve of Figure 4 of this discussion for a standard or even new lamination, we can easily obtain the winding for a first trial calculation. Usually the maximum torque is about the same as competition, but if not, it is easy to alter the winding. With this motor size and proportions perhaps Mr. Lloyd's method of working back from the maximum torque would apply very well.

The value C_T will vary with each manufacturer somewhat because of differences of winding tightness in the slots and often can only be determined after the first motor of a line has been carefully worked out. However, once C_T has been determined for one motor, Figure 1 will give the sizes of every other motor of the whole line within about five per cent.

The values of C_T as given in Table I are those we have worked out to make our motors about equal to the average of our competitors. If a quieter, more liberal, cooler, or more efficient motor line is wanted, C_T would be slightly greater, and if a noisier, more closely rated, or less efficient line would do for a special application then C_T would be slightly smaller. C_T is a function of the ventilation, and for totally enclosed motors is not as important as the amount of loss which the frame will dissipate.

C. W. Falls (General Electric Company, Schenectady, N. Y.): In Mr. Lloyd's paper mention is made of the lack of standards covering starting current and attention is called to the code letters established about two years ago to indicate starting kilovolt-amperes per horsepower for a-c motors. While it is true that no uniform power company standard on starting currents has ever been evolved, nevertheless the industry has made considerable advances in this matter, and the motor designer now has a good guide for this phase of his work.

The old National Electric Light Association rules referred to by Mr. Lloyd were really only recommendations of a NELA committee. While serving a useful purpose at the time the recommendations were issued in 1923, they were unduly restrictive, particularly for polyphase motors above 30 horsepower. They were not adopted officially by NELA and later were dropped by the power companies which realized that the values did not allow enough current to develop the necessary torques, nor did the recommendations reflect the ability of most polyphase circuits to accommodate motors with higher starting currents.

Table II. Three-Phase Motors

Horsepower	Locked Rotor Amperes at 220 Volts	Code Letter
1	27	*M*
1½	37	*L*
2	47	*K*
3	60	*J*
5	90	*H*
7½	120	*G*
10	150	*G*
15	220	*F*
20	290	*F*
25	365	*F*
30	435	*F*
40	580	*F*
50	725	*F*
60	870	*F*
75	1,085	*F*
100	1,450	*F*

Amperes of motors designed for other voltages will be inversely proportional to the voltage.

In recent years, motor manufacturers have become increasingly aware of their obligation to eliminate those designs having extremely high starting-current values. Also, they have realized the need for critically reviewing their designs with the idea of reducing motor starting currents in general to the lowest point consistent with obtaining the torque values demanded by motor users. In striving for consistently low values of starting current, the designer must strike a balance between starting current or starting kilovolt-amperes and suitable values of starting and breakdown torques. A line of motors will be definitely restricted as to general application if these torque values are unduly reduced by the designer in an effort to obtain starting-current values lower than those actually required by conditions usually existing on well designed polyphase power circuits.

Mr. Lloyd is familiar with and might have mentioned the starting-current values finally arrived at by a National Electrical Manufacturers Association committee with the previously mentioned considerations in mind. These values which have been followed by motor designers rather generally in the last three years are given in Table II of this discussion. It is believed that most polyphase power circuits are already capable of accommodating motors with these starting currents and, therefore, acceptance of these values should become rather general, with a very substantial saving to motor users in first cost of motor and control equipment, and hence, a definite benefit to the electrical industry as a whole.

In the paper, the standardization of code letters to indicate starting kilovolt-amperes per horsepower of a-c motors was mentioned. The table in paragraph 94304 of the National Electrical Code shows the values assigned to different code letters. These values were assigned by a NEMA committee at the request of electrical inspectors who wanted some indication on a-c motor nameplates of the starting current, so that proper branch-circuit protective devices might be more readily selected.

The extent to which motor designers have been able to obtain consistency of design with relatively low starting currents is indicated by the code letters shown in the starting current table. It should be noted that code *F* (5 to 5.59 kva per horsepower) is given for motors larger than ten horsepower and would generally apply to both normal starting torque and high starting torque types of polyphase motors.

Starting Windings for Single-Phase Induction Motors

Discussion and author's closure of paper 44-64 by C. G. Veinott, presented at the AIEE winter technical meeting, New York, N. Y., January 24–28, 1944, and published in AIEE TRANSACTIONS, 1944, June section, pages 288–94.

P. H. Trickey (Diehl Manufacturing Company, Elizabethport, N. J.): Mr. Veinott is to be commended for his very clear and thorough presentation of the starting equations for the different types of single-phase motors. The calculation form shown at the end of his paper has already proved of great value. That alone would justify presentation to the AIEE.

Different approaches are necessary when different limiting values are critical. For instance, Mr. Veinott stresses the case when the main winding current and line current are fixed, and shows how to get the maximum torque per ampere. This is a very common case in the design of general-purpose split-phase motors for household service. In the case of special-service motors, however, there are many cases when the required starting torque is known, but the line amperes are not known or particularly critical. In this case, an approach from the required torque is often useful. In my student paper at Rochester in 1931, to which Mr. Veinott has referred, I presented the formulas shown below with the exception of the correction for $X_a'/X_m' \neq K^2$, which are based on the "maximum torque" point, not the "maximum torque per ampere" point.

$$K = \frac{1.88p}{f}\left(\frac{E}{Z_m}\right)^2 r_{2m}' \frac{X_m'}{2(Z_m+R_m)} \times \frac{1}{(P_{La}/P_{Lm})}\,\frac{1}{T_s} \qquad (1)$$

$$r_{1a} = K^2 \frac{P_{La}}{P_{Lm}}(r_{1m}+Z_m) \qquad (2)$$

where

$$\frac{K^2P_{La}}{P_{Lm}} = \frac{X_a'}{X_m'} = \frac{2\pi f(C\,Kw_a)^2\,P_{La}}{2\pi f(C\,Kw_m)^2 P_{Lm}}$$

(It is interesting to note that these agree with the formulas of Mr. Lloyd and Mr. Veinott when $X_a'/X_m' = K^2$.)

In actual practice, however, I find it possible to use this method to obtain a winding very close to the "maximum torque per ampere" winding for those cases where the line current is not known. Referring to Figure 1 of this discussion, from the student paper, we simply use in equation 1 a torque value of five or ten per cent higher than that required. Using the slightly low value of *K*

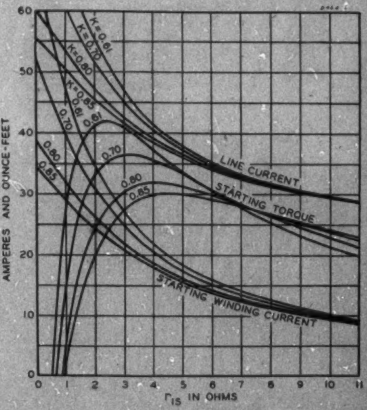


Figure 1. Effect of varying the number of turns and resistance of the starting winding upon the starting torque and current of a split-phase motor

$$K = \frac{\text{effective turns of starting winding}}{\text{effective turns of main winding}}$$

obtained, we solve equation 2 for r_{1a}. Then instead of using this value exactly, we take the next smallest wire size. If the motor is not too critical, this is sufficiently close to a satisfactory winding. If the motor is critical, we would always investigate a number of trials around these values using Mr. Veinott's calculation sheet.

Cyril G. Veinott: Mr. Trickey makes the excellent point that often it is torque which is specified, not the locked-rotor current. It is obvious from Figure 3 of the paper, as well as from Mr. Trickey's discussion, that it is always desirable to use more resistance in the starting winding than required for a "maximum-torque" winding. Using Mr. Trickey's philosophy, the following variation is suggested as being somewhat more scientific:

1. Compute the required value of K in the manner outlined by Mr. Trickey, from the required starting torque.
2. From this value of K, compute X_a'.
3. Compute R_a from equation 109 or 20.
4. If the winding selected corresponds to the value of R_a found above, the line current can be readily computed, using equation 17.

Or, for routine design calculations, when a form such as Figure 6 is available, the following procedure is very convenient: (1) enter all the items pertaining to the main winding; (2) after computing X_a' and K to give the required starting torque, as outlined by Mr. Trickey, enter these items and compute item 19 from equation (109*a*); (3) since item 18 can then be computed, the remainder of the items can be computed, yielding the complete specification and performance of the winding.

A Graphical Analysis of the Voltage and Current Wave Forms of Controlled Rectifier Circuits

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Synopsis: Controlled rectifiers employing gaseous discharge tubes are used more and more frequently in the industrial field owing to the ease of control of the output voltage and the high conversion efficiency. The voltage and current wave forms at the different parts of controlled rectifier circuits are often non-sinusoidal and discontinuous due to the one-way valve-action of the rectifying elements. This paper presents a graphical method of constructing the voltage and current wave forms based on a few well-known fundamental principles of electrical engineering. This method is presented step by step and a few typical examples are shown to illustrate its validity.

1. Introduction

CONTROLLED rectifiers, either thyratron-tube or ignitron-tube equipments, are becoming more and more frequently used to obtain regulated sources of d-c from an a-c supply, both for d-c power requirements and for control purposes.

When the regulated source of d-c requires only a few kilowatts of power, the single-phase rectifier circuit is preferable because of its economical use of tubes and associated apparatus and because of the simplicity of its control circuits. In addition, the single-phase circuit is probably easier for the beginner to understand, and once its fundamental principles of analysis are mastered they can be extended readily to polyphase rectifier circuits.

Comparatively little material is available in current literature on the subject of controlled rectifiers, and in particular on the analysis of voltage and current wave forms at different parts of the circuit. Rectifier-circuit analysis is difficult because nonsinusoidal wave forms, which often involve discontinuities, are produced by the one-way controlled-valve action of the gaseous-discharge-tube rectifying elements.

A knowledge of these voltage and current wave forms is necessary to predict the nature of the output which can be obtained from a particular circuit and load combination, and to select elements of the correct ratings for the voltages and currents encountered. Furthermore, a mathematical analysis of these circuits presupposes a physical picture of the circuit behavior.

In order to show the significance of voltage and current wave forms with respect to each circuit element, consider the single-tube rectifier supplying a load comprising an inductance in series with a resistance as shown in Figure 1a. Such a load is typical of the field winding of a dynamo-electric machine, the coil of a contactor or relay, and the windings of clutches and solenoids. Figure 1b shows the instantaneous distribution of voltage across the four elements of the circuit, namely, anode-transformer secondary winding, rectifying element, inductance, and resistance.

Figure 2a to e, inclusive, shows the wave forms of voltage and current at the individual elements of the circuit of Figure 1. Figure 2b is the wave form of the output voltage of this half-wave rectifier circuit. In practice, as in the case of a coil whose inductance has inevitable resistance, the voltage across L cannot be segregated from the voltage across R so that each voltage can be observed separately. It is from the picture of Figure 2b that the wave forms of Figure 2c, d, and e are most readily derived.

Table I summarizes the significance of each picture of Figure 2 with respect to those features which are important to each circuit element.

In the presentation which follows, each of the fundamental principles is stated and the examples are analyzed by the application of these principles. The analysis is based on the following assumptions:

A. Linear circuit elements, R–L–C.

B. Transformer leakage reactance and resistance and the internal resistance and reactance of a counter electromotive force are neglected.

C. The solution is that of the steady state, although this method can be extended to transient analysis as is shown by the example of Figure 14a.

2. Fundamentals of Analysis

The several fundamental principles of circuit analysis which will be used throughout this discussion are stated herewith. Each principle is given a number by which it is referred to in the examples which follow.

2.1. KIRCHHOFF'S LAWS

2.11. *Voltage*. The summation of the instantaneous voltages around a closed circuit is equal to zero.

Referring to Figure 1b, it is shown that the instantaneous transformer voltage is equal to the sum of the respective voltages, at that same instant, across the tube, the inductance, and the resistance.

2.12. *Current*. The summation of the instantaneous currents at a given point equals zero.

2.2. THE RESISTOR—ITS VOLTAGE-CURRENT RELATIONS

A pure resistor cannot store energy; it can only dissipate energy.

The energy consumed by a resistor is dissipated in the form of heat. This energy is expressed in watt-seconds, or joules, if e_R is in volts and i in amperes according to the relation:

$$\text{Energy} = \int e_R i \, dt$$

The power consumed by a resistor, or, the rate of energy dissipation, is equal to

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$e_R i$ and is measured in watts if e is expressed in volts and i in amperes.

2.21. Both the current flowing through a resistor and the voltage across the resistor can be changed instantaneously.

2.22. The voltage drop across a resistor is always of such polarity as to oppose the flow of current through the resistor.

2.23. In a resistor, the voltage and the current have the following relation:

$e_R = R\,i$

2.3. THE INDUCTOR—ITS VOLTAGE-CURRENT RELATIONS

A pure inductor cannot dissipate energy.

Energy can be stored in the electromagnetic field of an inductor so long as current is flowing in the winding. This energy is expressed in watt-seconds if the inductance is in henrys and the current in amperes according to the relation:

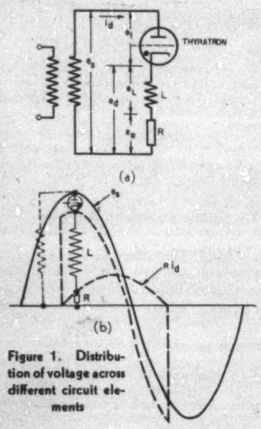


Figure 1. Distribution of voltage across different circuit elements

Energy $= {}^1/_2 L i^2$

Thus, in a given inductance of L henrys, the square of the current at any instant is a measure of the energy stored therein.

2.31. The energy stored in an inductor cannot be changed instantaneously; therefore, the current through the inductor cannot be changed instantaneously.

The voltage across an inductor can be changed instantaneously (a change of voltage is not a change of energy).

2.32. ***Lenz's Law.*** **The polarity of the induced voltage across an inductor is always such as to oppose the change of current through the inductor.**

That is, the polarity of the induced voltage cannot be determined from the direction of flow of current alone, but also depends on whether the current is increasing or decreasing. With the direction of flow of current as in Figure 3a, if the current is increasing the polarity of the induced voltage will be as shown in Figure 3b; if the current is decreasing the polarity will be as shown in Figure 3c; if the current is not changing there will be no induced voltage. The induced voltage is always equal and opposite to the impressed voltage.

2.33. In an inductor, the impressed voltage and the current have the following relation:

$$e_L = L\frac{di}{dt}$$

From this fundamental equation several useful conclusions can be drawn concerning the relation between the impressed voltage and the current of an inductor.

2.331. The slope of the current (di/dt in amperes per second) at any point equals the inductor voltage (volts) divided by the inductance (L henrys).

2.332. The current is either a maximum or a minimum when the inductor voltage is equal to zero.

Proof: i = maximum or minimum when $di/dt = 0$, that is, when $e_L = 0$

In Figure 4, the currents at t_2, t_6, t_{10}, t_{14} are maxima; the currents at t_4, t_8, t_{12} are minima. The voltages corresponding to these currents are equal to zero.

2.333. The current has a point of inflection when the voltage is either a maximum or a minimum.

Proof: If e_L = maximum or minimum, then the slope at that point must be either a maximum or a minimum.

In Figure 4, the current has a point of inflection at t_1, t_3, t_5, t_7, t_9, t_{11}, t_{13} and the inductance voltage has either a maximum or a minimum at these points.

2.334. The change of current from a value i_1 at a time t_1 to a value i_2 at a time t_2 is equal to the time integration of the inductance voltage between t_1 and t_2 (expressed in volt-seconds) divided by the inductance (expressed in henrys).

Proof:

$$\int_{i_1}^{i_2} di = \frac{1}{L}\int_{t_1}^{t_2} e_L dt$$

$$i\Big]_{i_1}^{i_2} = \frac{1}{L}\int_{t_1}^{t_2} e_L\,dt = i_2 - i_1$$

In Figure 4, the difference between the currents at t_2 and at t_6, that is, $i_6 - i_2$, is equal to

$$\frac{1}{L}\left(-A_2 + A_3\right)$$

2.3341. The current at any instant t_2 is equal to the time integration of the voltage divided by the inductance if the current equals zero at t_1.

Proof:

$$i_2 = \int_{t_1}^{t_2} e_L dt \text{ because } i_1 = 0$$

In Figure 4, $i_2 = \dfrac{A_1}{L}$ since $i_1 = 0$.

2.3342. If the current at t_1 is equal to the current at t_2, the time integration of the voltage during the interval between t_1 and t_2 must be equal to zero.

Proof: if $i_1 = i_2$ then

$$\frac{1}{L}\int_{t_1}^{t_2} e_L dt = i_2 - i_1 = 0$$

In Figure 4, if $i_{10} = i_{14}$, then $A_6 = A_7$.

2.335. The current wave is a smooth curve (no sudden change of slope) if there is no sudden change of voltage. The current wave is not a smooth curve (sudden change of slope) if there is a sudden change of voltage.

The current wave of Figure 4 is a smooth curve since there is no sudden change of voltage. The current wave of Figure 5 is not a smooth curve since there is a sudden change of voltage at t_3, t_6, t_9.

2.4. THE CAPACITOR—ITS VOLTAGE-CURRENT RELATIONS

A pure capacitor cannot dissipate energy.

Energy can be stored in the electrostatic field of a capacitor so long as a potential is present across its terminals. This energy is expressed in watt-seconds if the

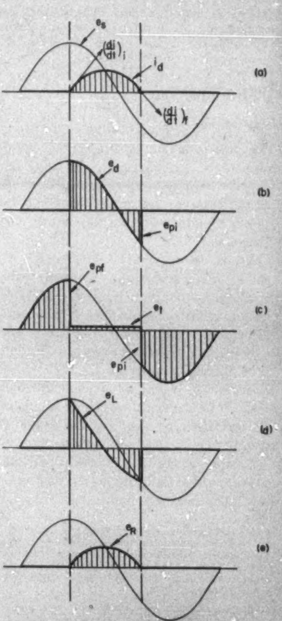


Figure 2. Voltage and current wave forms

Table I. Significant Features of Wave Forms of Figure 2 With Respect to Each Circuit Element

Figure	Tube	L	R	Transformer
2a	Peak current Average current Initial[1] and final rate change of current	Average current	Rms current	Rms current
2b		Average voltage output (actually L and R are inseparably combined as in coil)		
2c	Peak inverse voltage conduction period			
2d		Peak voltage		
2e			Same as Figure 2a plotted in terms of iR drop	

capacitance is in farads and the potential in volts according to the relation:

$$\text{Energy} = {}^1/_2 Ce_C{}^2$$

Thus, in a given capacitance of C farads, the square of the voltage at any instant is a measure of the energy stored therein.

2.41. The energy stored in a capacitor cannot be changed instantaneously; therefore, the potential across the capacitor cannot be changed instantaneously.

The current "through" a capacitor can be changed instantaneously (a change of current is not a change of energy).

2.42. The direction of flow of current in a capacitor cannot be determined from the polarity of the voltage across the capacitor alone, but also depends on whether the voltage is increasing or decreasing.

The voltage across the capacitor always changes in such a way as to oppose the flow of current through the capacitor. Therefore, in Figure 6b the current flows from "2" to "1" if the voltage is increasing; current flows from "1" to "2" if the voltage is decreasing; if the voltage is not changing there will be no current flow.

2.43. In a capacitor, the voltage and the current have the following relation:

$$= C\frac{de_C}{dt}$$

From this fundamental equation several useful conclusions can be drawn concerning the relation between the voltage and the current of a capacitor.

2.431. The slope of the voltage (de_C/dt, in volts per second) at any point equals the capacitor current (amperes) divided by the capacitance (C farads).

2.432. The voltage is either a maximum or a minimum when the current is equal to zero.

Proof: e_C = maximum or minimum when $de_C/dt = 0$, that is, when $i = 0$

In Figure 8, the voltage e_{C1} at t_1 is a maximum and the current i_1 at t_1 is equal to zero.

2.433. The voltage has a point of inflection when the current is either a maximum or a minimum.

Proof: If i = maximum or minimum, then the slope of the voltage must be either a maximum or a minimum.

In Figure 7, the voltage wave at t_1, t_4, t_7, t_{10} has a point of inflection when the current has maximum values at these corresponding instants.

2.434. The change of voltage from a value e_1 at a time t_1 to a value e_2 at a time t_2 is equal to the time integration of the capacitance current between t_1 and t_2 (expressed in ampere-seconds, or coulombs) divided by the capacitance (expressed in farads).

Proof:

$$\int_{e_C}^{e_{C2}} de_C = \frac{1}{C}\int_{t_1}^{t_2} i\,dt$$

$$e_C\Big]_{e_{C2}}^{e_{C1}} = \frac{1}{C}\int_{t_1}^{t_2} i\,dt = e_{C2} - e_{C1}$$

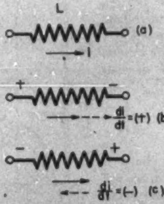


Figure 3. Determination of the polarity of the induced voltage in an inductor

In Figure 8, the difference between the voltages at t_0 and at t_3, that is, $e_{C3} - e_{C0}$, is equal to

$$\frac{1}{C}(+A_1 - A_2)$$

2.4341. The voltage at any instant t_4 is equal to the time integration of the current divided by the capacitance if the voltage equals zero at t_1.

Proof:

$$e_{C2} - e_{C1} = \frac{1}{C}\int_{t_1}^{t_2} i\,dt = e_{C2}$$

because $e_{C1} = 0$.

In Figure 7, $e_{C1} = \frac{A_1}{C}$ since $e_{C0} = 0$.

2.4342. If the voltage at t_1 is equal to the voltage at t_2, the time integration of the current during the interval between t_1 and t_2 must be equal to zero.

Proof: if $e_{C1} = e_{C2}$ then

$$\int_{t_1}^{t_2} i\,dt = e_{C2} - e_{C1} = 0.$$

In Figure 8, if $e_{C3} = e_{C5}$ then $A_3 = A_4$.

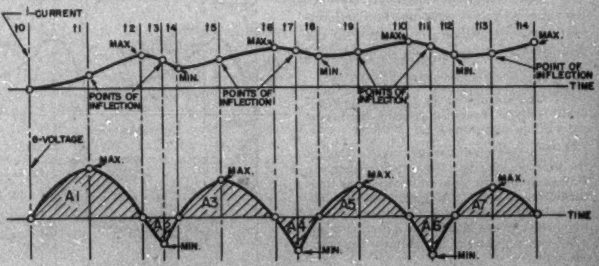
2.435. The voltage wave is a smooth curve (no sudden change of slope) if there is no sudden change of current. The voltage wave is not a smooth curve (sudden change of slope) if there is a sudden change of current.

The voltage wave of Figure 7 is a smooth curve since there is no sudden change of current. The voltage wave of Figure 8 is not a smooth curve since there is a sudden change of current at t_0, t_3, t_6.

2.5. THE RECTIFYING ELEMENT—ITS VOLTAGE-CURRENT RELATIONS

The rectifying element considered throughout this discussion is of the gaseous-discharge type—the thyratron or the ignitron.

Figure 4 (below). Graphs showing the relation between the voltage across and the current through an inductor



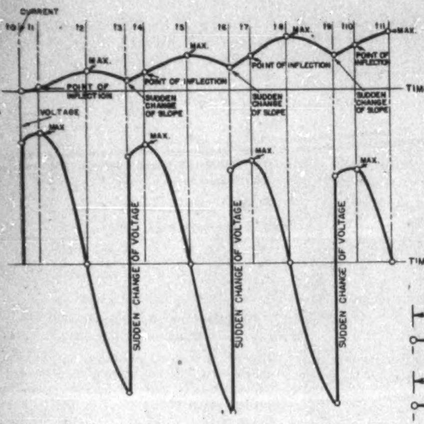


Figure 5. Graphs showing the relation between the voltage and current in an inductor (sudden change of voltage)

2.51. Before the tube fires and before the current begins to flow, the tube acts as an open switch.

It is assumed that the striking voltage at which gaseous conduction begins is the arc-drop voltage (actually, the striking voltage may be more than arc-drop voltage,[2] say two or three times arc drop voltage). That is, it is assumed that gaseous conduction (as distinguished from electron space current before ionization occurs) begins as soon as the anode is positive with respect to the cathode by an amount equal to the arc drop.

In a thyratron, the actual firing point[3] is dependent upon anode voltage, grid voltage, and tube characteristic. In this discussion, the manner of controlling the firing point is incidental; only the results of the controlled tube as a circuit element are considered.

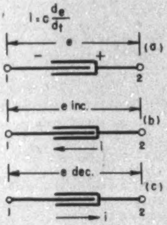
2.52. After the tube fires, the voltage across the tube is its arc drop which is practically independent of current. In this discussion, the arc drop during conduction is assumed to be constant.[4]

After the tube fires, the current through the tube is limited only by the combination of circuit elements in series with the tube, including any counter electromotive forces.

2.53. The voltage across the tube is equal to the voltage across the transformer minus the voltage across the load.

2.54. The gaseous discharge current through the tube ceases when the circuit causes the voltage across the tube to become less than the tube's arc drop. (In ignitron tubes without holding-anodes, the anode current ceases when the current falls below a certain minimum value necessary to maintain the cathode spot.)

Figure 6 (below). Determination of the direction of current flow in a capacitor



2.6. THE LOAD—ITS VOLTAGE-CURRENT RELATIONS

The load is a combination of R, L, C with or without a counter electromotive force.

2.61. The voltage across the load, when a tube is conducting, follows the supply-voltage wave-form (minus arc drop).

2.62. The voltage across the load, when no tube is conducting, depends on the nature of the load and whether a counter electromotive force or parallel load path is present. This is shown in Figures 9a, b, and c, respectively.

2.63. The relation between voltage and current at the load is complicated, but the approximate wave form of the current, in relation to the voltage, can be sketched by the application of these principles.

3. Examples

3.1. The pure inductance of L henrys (zero resistance) supplied from a single-tube, half-wave, controlled rectifier, fired at 60 degrees and having a peak anode voltage of E_{ps} volts. This circuit is shown in Figure 10k.

PROCEDURE

3.11. Sketch in the sine wave of secondary voltage e_s as in Figure 10a.

3.12. Then, the extinction point of the current will be $360-60$ degrees since equal positive and negative areas of voltage must appear across the inductor whose current begins at zero and ends at zero. Figure 10b. Principle 2.3342.

3.13. Draw the voltage wave across the inductor. Figure 10c. Principle 2.6.

3.14. Sketch the inverse voltage of the tube. This is the transformer voltage minus inductor voltage. Figure 10d. Principle 2.53.

3.15. Determine the initial slope of the current wave. Principle 2.33.

This may be done graphically by constructing a right-angle triangle whose verti-

Figure 7. Graphs showing the relation between the voltage and the current in a capacitor (no sudden change of current)

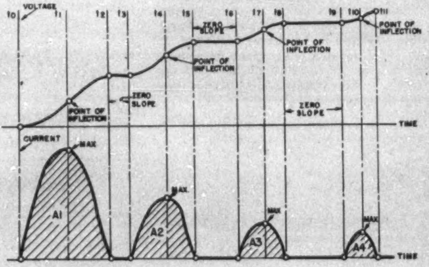
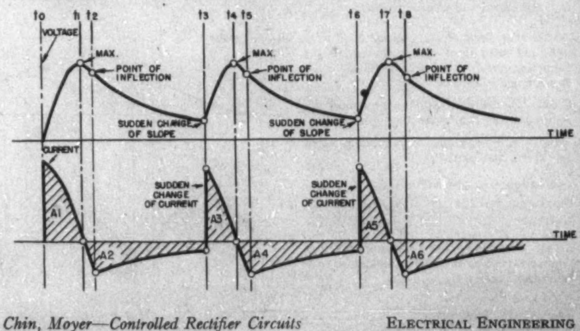


Figure 8 (below). Graphs showing the relation between the voltage and current in a capacitor



cal side is the voltage at this instant and whose horizontal side is L. Choose a convenient length for L, what length merely determines the scale of the current curve. Figure 10e. Principle 2.331.

3.16. Determine the final slope of the current wave in the same manner as for the initial slope. Figure 10e.* Principle 2.331.

3.17. Determine the peak value of the current wave (principle 2.3341) which occurs at the 180-degree point (principle 2.332) by calculating or estimating (for example, counting squares) the area underneath the positive half cycle of inductor voltage and dividing this area by the inductance. Figure 10f.

3.18. If necessary, additional points for the curve can be obtained by estimating the area up to a given point and dividing by L. Figure 10g and h. Principle 2.334.

3.19. The effect of arc drop can be determined from principle 2.52 by subtracting volts of arc drop from the inductor voltage. Figure 10i.

The peak current now occurs before the 180-degree point and this current wave is unsymmetrical, that is, the final slope differs from the initial slope. Figure 10j.

3.191. Figure 10l, m, and n show the voltage and current wave forms of a pure inductance supplied by a single-tube half-wave rectifier fired at different points.

3.2. An inductance of L henrys and a resistance of R ohms in series supplied from a single-tube half-wave controlled rectifier, fired at 60-degrees and having a peak anode voltage of E_{ps}, volts. This is the circuit of Figure 11h.

PROCEDURE†

3.21. Sketch in the sine wave of secondary voltage e_s as in Figure 11a.

3.22. Find the initial slope of the IR-voltage wave (which is proportional to the current). Principle 2.331. In this instance, the slope is constructed from the right-angle triangle whose vertical height is E_{pf}, volts and whose horizontal length is L/R seconds. Figure 11b.

3.23. Assume a peak value of IR voltage which peak value will fall along the positive-decaying-portion of the inductor voltage wave. Sketch in the IR-voltage wave, roughly. Its slope will be horizontal at the point of crossing the inductor voltage wave. Figure 11c. Principle 2.332.

3.24. Measure the area between the inductor-voltage curve and the IR-voltage curve. Divide this area (volt-seconds) by the time constant L/R (seconds) to determine the peak value of IR. If this calculated value of IR does not agree closely with the assumed value of 3.23 repeat 3.23 and 3.24 with a more accurate value until the value chosen in 3.23 and the value calculated in 3.24 agree closely. Figure 11d. Principle 2.334.

3.25. Find the extinction point by assuming a point such that the negative voltage area between the IR curve and the e_s curve equals the positive area of 3.24. Figure 11e. Principle 2.3342.

If the first assumption is not correct, repeat with more accurate values until the areas are equal.

3.26. If more accuracy is required, intermediate points can be drawn as shown in Figure 11f and g.

3.27. The inverse voltage across the tube can be drawn as in 3.14.

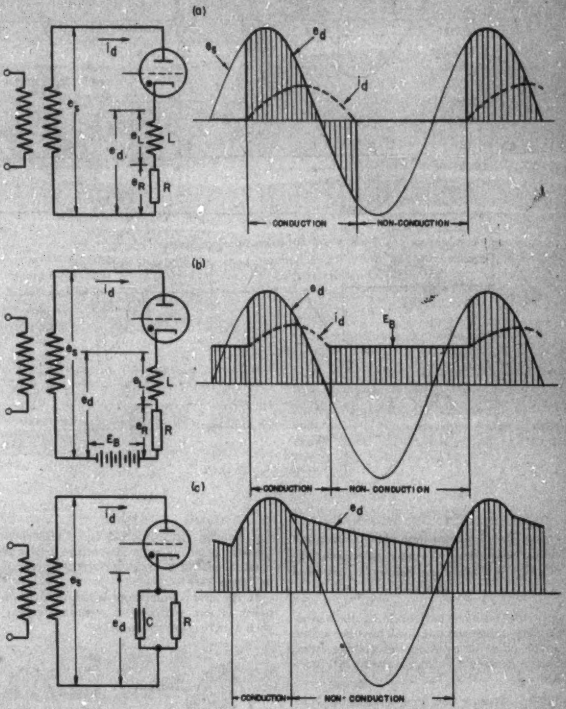


Figure 9. Voltage across the load

3.28. The effect of tube drop can be shown by subtracting arc drop from the inductor-voltage wave as in paragraph 3.19, then re-estimating the areas based on the new inductor-voltage wave.

3.3. The L–R load with counter electromotive force. Supplied from a two-tube biphase half-wave controlled rectifier, fired at 60 degrees and having an anode-to-neutral voltage of E_{ps}, volts. This circuit is shown in Figure 12g.

3.31. Sketch the transformer-voltage waves e_{S1} and e_{S2} and draw in the counter-electromotive force voltage to the same scale as in Figure 12a.

3.32. Test for current continuity as follows:

3.321. If the current is continuous, the total load voltage should be like that of Figure 12b.

Find the average value of this voltage wave. This average will be a value which will give equal positive and negative shaded areas, as shown.

For current to be continuous, the end of the negative shaded area should coincide with the beginning of the successive positive area.

If the counter electromotive force is higher than this average voltage level, continuous current will be impossible. This is shown in Figure 12f.

3.322. If the current is discontinuous, the total load voltage should look like that of Figure 12f which has very definite regions of zero current when the output voltage is the counter electromotive force. Principle 2.62.

The average value of this voltage wave will be found to be higher than the counter-electromotive force level.

3.33. Assuming the discontinuous current case of paragraph 3.322, draw the transformer voltage waves and the counter electromotive force as in Figure 12a.

3.331. Draw the initial slope of the IR voltage wave as in Figure 12c.

3.332. Assume a peak value of the IR voltage wave. This peak value will fall along the

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* In Figure 10e, the value of L was taken to be so many units of length. Each square of area is volttage height times a base width, so, the division of total squares of area by base divisions of L will give a measure of current.

† In the sketches which follow, the current is proportional to IR drop and is sketched in terms of IR drop.

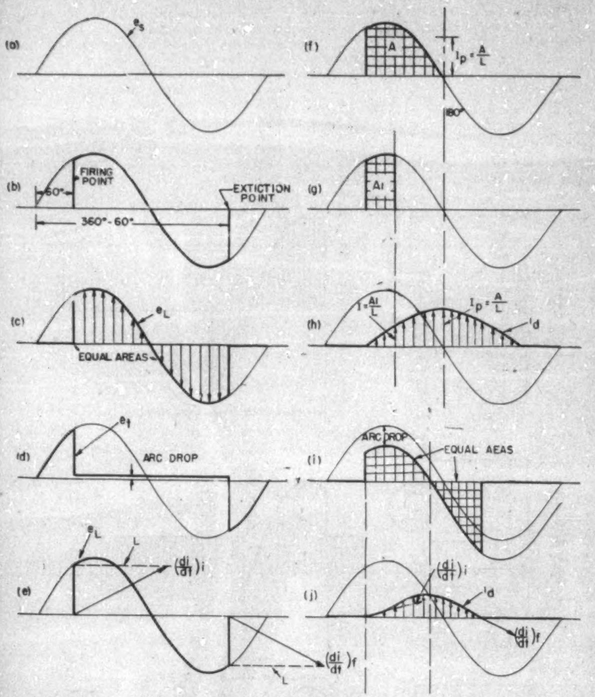

(a)
e_s
(b)
FIRING POINT
60°
EXTICTION POINT
360°-60°
(c)
e_L
EQUAL AREAS
(d)
e_t
ARC DROP
(e)
e_L
L
$(\frac{di}{dt})_i$
$(\frac{di}{dt})_f$
L
(f)
A
$I_p = \frac{A}{L}$
180°
(g)
A1
(h)
$I = \frac{A1}{L}$
$I_p = \frac{A}{L}$
i_d
(i)
ARC DROP
EQUAL AEAS
(j)
$(\frac{di}{dt})_i$
i_d
$(\frac{di}{dt})_f$

Figure 10a–j. Construction of the voltage and current wave forms of a pure inductance supplied by a single-phase half-wave controlled rectifier

decaying positive half cycle of the transformer-voltage wave and will have zero slope at the point of crossing this voltage wave. Sketch in the *IR* voltage wave as in Figure 12d.

3.333. Measure the area of inductor voltage and divide by L/R to determine the peak value of *IR* voltage at this point. If this calculated value of *IR* does not agree with the assumed value of 3.332, repeat 3.332 and 3.333 using an assumed value which is more accurate. Figure 12d.

3.334. Determine the extinction point of the *IR* voltage wave. This point will lie on the counter electromotive force line such that the negative inductor-voltage area will equal the positive inductor-voltage area as in Figure 12e.

If the first assumption of the extinction point is not sufficiently accurate, repeat with a more accurate value.

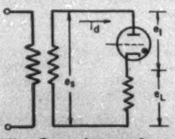


Figure 10k. Circuit diagram of a single-phase half-wave controlled rectifier supplying a purely inductive load

As a further refinement, intermediate points can be determined as in paragraph 3.26.

3.335. Check the final slope of the current wave as being the triangle formed by e_s and L/R.

3.336. If desired, the effect of tube drop can be found as in paragraph 3.19.

3.337. The tube inverse voltage can be found as in paragraph 3.14.

3.338. Find the average output voltage by averaging the counter electromotive force plus the *IR* drop.

3.34. Assuming the continuous-current case of paragraph 3.321, draw the transformer voltage waves, the firing point and corresponding average voltage line, the voltage across the load, and the counter electromotive force line as in Figure 13a.

3.341. Construct the *IR* drop about the average voltage line. This is done by assuming an initial value of *IR* drop which is approximately, but not exactly, midway between the average voltage line and the counter electromotive force line. This initial *IR* drop is therefore negative with respect to the average voltage line (that is, below the average voltage line) but positive with respect to the counter electromotive force line (that is, above the counter electromotive force line).

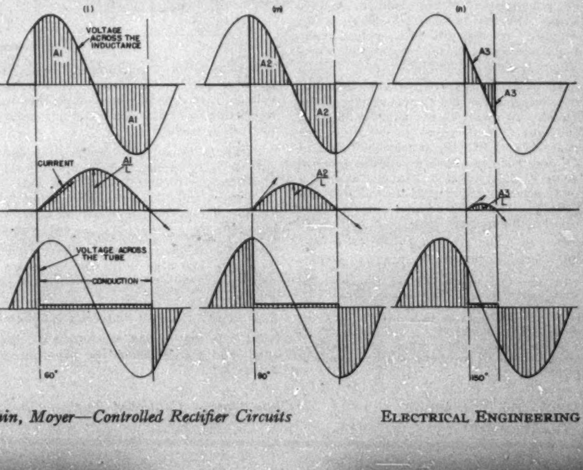
3.342. Draw the initial slope of the *IR* voltage wave based on this assumed initial value of *IR* drop. Figure 13b.

3.343. Assume a peak value of *IR* voltage which will fall along the positive-decaying portion of the transformer voltage wave and will have zero slope at the point of crossing this voltage wave.

Sketch in the *IR* voltage wave as in Figure 13b and c.

3.344. Measure the area of inductor voltage. Divide this area, A_1 in Figure 13d, by

Figure 10l–n. Voltage and current wave forms of a single-phase half-wave rectifier supplying a pure inductance firing at different points


(l)
VOLTAGE ACROSS THE INDUCTANCE
A1
A1
CURRENT
$\frac{A1}{L}$
VOLTAGE ACROSS THE TUBE
CONDUCTION
60°
(m)
A2
A2
$\frac{A2}{L}$
90°
(n)
A3
A3
$\frac{A3}{L}$
150°

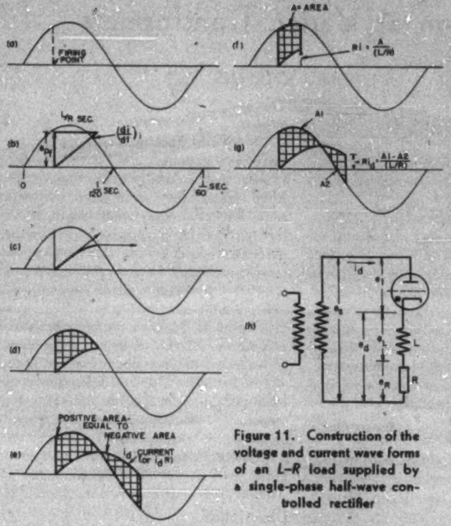

(a)
FIRING POINT
(b)
L/R SEC.
$\left(\frac{di}{dt}\right)_1$
e_{D1}
0
$\frac{1}{120}$ SEC.
$\frac{1}{60}$ SEC.
(c)
(d)
(e)
POSITIVE AREA-EQUAL TO NEGATIVE AREA
i_d CURRENT (or $i_d R$)
(f)
A = AREA
$Ri = \frac{A}{(L/R)}$
(g)
A1
A2
$I_2 - Ri_d = \frac{A1-A2}{(L/R)}$
(h)
i_d
e_1
e_s
e_d
e_L
e_R
L
R

Figure 11. Construction of the voltage and current wave forms of an *L–R* load supplied by a single-phase half-wave controlled rectifier

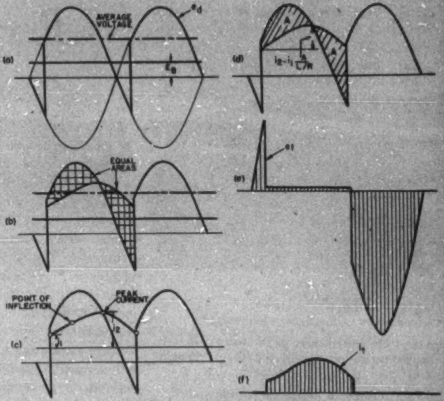

(a)
AVERAGE VOLTAGE
e_d
E_B
(b)
EQUAL AREAS
(c)
POINT OF INFLECTION
PEAK CURRENT
i_1
i_2
(d)
A
A
$i_2 - i_1 = \frac{A}{L/R}$
(e)
e_1
(f)
i_1

Figure 13. Construction of the voltage and current wave forms of *L*, *R*, and E_B in series supplied by a biphase half-wave controlled rectifier (continuous current)

L/R seconds to obtain the increased value of *IR* at this instant. The increased value of *IR* is measured from the level of the initial value.

If this estimated value of *IR* does not agree with the assumed value of paragraph 3.343, repeat the operation of paragraph 3.343 with a more accurate value.

3.345. The extinction point of *IR* drop must coincide with the initial value of *IR* drop as in paragraph 3.341.

Check that the positive and negative inductor-voltage areas are equal as in Figure 13d. Make repeated tries until sufficient correspondence is achieved.

3.346. Check the final slope of the *IR* curve by dividing the final *IR* drop by *L/R*.

3.347. As a check, the average value of the counter electromotive force plus *IR* drop should equal the average output voltage of the rectifier when fired at the assumed 60-degree point.

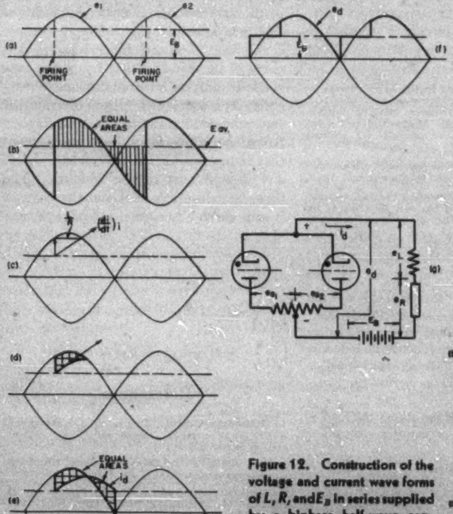

(a)
e_1
e_2
E_B
FIRING POINT
FIRING POINT
(b)
EQUAL AREAS
E av.
(c)
$\left(\frac{di}{dt}\right)_1$
(d)
(e)
EQUAL AREAS
i_d
(f)
e_d
E_b
(g)
i_d
e_d
e_L
e_R
e_{s_1}
e_{s_2}
E_B

Figure 12. Construction of the voltage and current wave forms of *L*, *R*, and E_B in series supplied by a biphase half-wave controlled rectifier (discontinuous current)

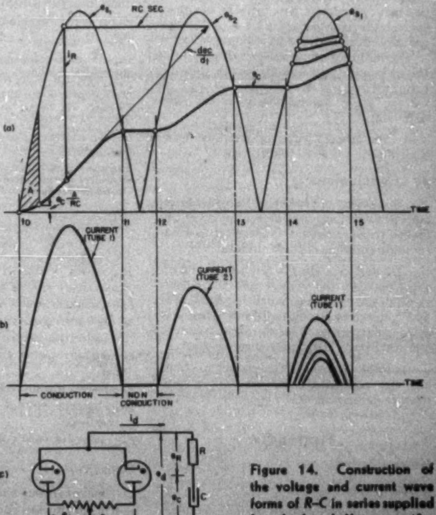

(a)
e_{s_1}
RC SEC.
e_{s_2}
e_{s_1}
i_R
$\frac{de}{dt}$
e_c
A
$e_c = \frac{A}{RC}$
TIME
t0
t1
t2
t3
t4
t5
(b)
CURRENT (TUBE 1)
CURRENT (TUBE 2)
CURRENT (TUBE 1)
TIME
CONDUCTION
NON CONDUCTION
(c)
i_d
e_R
e_d
e_c
R
C
e_{s_1}
e_{s_2}

Figure 14. Construction of the voltage and current wave forms of *R–C* in series supplied by a biphase half-wave rectifier

3.348. As another check, the IR drop should have equal areas about the average voltage line.

3.349. The effect of tube drop can be considered as in paragraph 3.19.

Tube inverse voltage can be found as in Figure 13e.

Note the double inverse voltage wave form.

Tube current is shown in Figure 13f.

3.4. A capacitor of C farads in series with a resistor of R ohms and supplied from a two-tube bi-phase half-wave rectifier whose peak anode voltage is E_{ps}, volts. This circuit is shown in Figure 14c.

Figure 14a shows the transient build-up of voltage across the capacitor when started from an initial, discharged condition.

3.41. Sketch the positive half-cycles of transformer voltages as in Figure 14a.

3.42. The initial slope of the voltage is zero since the initial current is zero. Principle 2.431.

3.43. Assume a capacitor voltage at the end of the first half-cycle of conduction at t_1, and sketch in the capacitor voltage wave.

3.44. Measure the area corresponding to the time integration of IR voltage. Divide this area (expressed in volt-seconds) by the time constant RC (expressed in seconds) to determine the capacitor voltage at t_1. It should check with the value assumed in paragraph 3.43; if not, repeat the procedure with a more accurate value in paragraph 3.43.

For more accurate results, a few intermediate points can be estimated by integrating the IR drop only up to these points.

3.45. At t_1 tube 1 ceases to conduct and tube 2 will not begin to conduct until e_{s2} is equal to e_{c1}. Starting from t_2, tube 2 will conduct. Assume a new increase of capacitor voltage as in paragraph 3.43 and repeat the procedure outlined in paragraph 3.44.

The build-up of capacitor voltage during the successive four half-cycles are shown in the third half-cycle of Figure 14a with the voltage wave superimposed on the same half-cycle.

3.46. The current wave forms can be drawn from Figure 14a as in Figure 14b.

4. Conclusions

A. The ability of predicting the voltage and current wave forms at different parts of controlled rectifier circuits is essential to a better understanding of these circuits.

B. By understanding a few basic relationships between the voltage and the current through the circuit elements—L, R, C, tube, and counter electromotive force—the voltage and current wave forms can be constructed graphically to a fairly accurate degree.

C. It is the intention of this paper to stimulate the general interest so that a more rigorous treatment of the controlled rectifier circuits will be made.

Appendix

Nomenclature

VOLTAGE

e_s—instantaneous voltage on the secondary side of the anode transformer

E_{ps}—peak value of the voltage on the secondary side of the anode transformer

e_R—instantaneous voltage across a resistor

e_L—instantaneous voltage across an inductor

e_C—instantaneous voltage across a capacitor

e_T—instantaneous voltage across the rectifier

e_d—instantaneous voltage across the load

E_B—battery voltage or counter electromotive force

e_{pf}—peak forward voltage

CURRENT

i_d—instantaneous current through the load

i_T—instantaneous current through the rectifier

$\left(\frac{di}{dt}\right)_i$—initial rate of change of current

$\left(\frac{di}{dt}\right)_f$—final rate of change of current

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Design of Variac Transformers

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MEMBER AIEE

MOST textbooks on transformers mention in a few concluding paragraphs that for certain applications special transformers can be used to good advantage. Foremost among these is the autotransformer which offers very important advantages if the ratio of input to output voltage is close to unity. Other special transformers mentioned are small instrument transformers, constant-current transformers, and voltage regulators. Voltage regulators are ordinarily divided into several groups: the automatic magnetic-flux-type regulator without moving parts, the induction regulator with one of its windings mounted so that it can be turned into different positions with respect to the other windings, and the tap-changing type, which requires special precautions if voltage has to be changed under load.

In power-distribution systems, a certain amount of flexibility is required to provide constant voltage to the ultimate consumer regardless of his distance from the station and regardless of load, and some feeder lines are connected to individual hand-operated or automatic regulators. The regulators are ordinarily tap-changing autotransformers or boosters, or they are induction regulators. While variable-ratio transformers of high rating have been manufactured for these applications by practically every one of the large transformer companies, smaller units have not been available until recently. It is the purpose of this paper to describe a variable-ratio transformer[1] intended primarily for low power. When the first Variac, the continuously adjustable variable-ratio autotransformer, was introduced in 1933, it found immediate acceptance. Today close to one million transformers of this type are in use. They are available from several manufacturers with ratings from 100 to 10,000 volt-amperes. Three transformers, as manufactured by General Radio Company, Cambridge, Mass., are shown in Figures 1–3.

Description

As the name Variac implies, the new transformer provides "variable a-c voltage" when connected to a power line. The basic principle is that of a tap-changing autotransformer. The tap-changing mechanism is the essential part of this transformer and the simplicity of this mechanism is responsible for its success. The "taps" are formed by exposed parts of the winding that is applied in a single layer over a toroidally shaped-iron core. The number of taps is the same as the number of turns of the transformer. Rotation of the control shaft causes a contact to traverse these turns and to tap off any desired part of the winding. The turns are closely spaced and the contact is wide enough to engage at least one turn

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The analysis of optimum contact resistance in the equivalent contact circuit has been contributed by Doctor W. N. Tuttle.

at all times. It is possible, therefore, to move the contact under load without interrupting the circuit. To engage at least one turn at all times, the contact must be wider than the spacing between turns and it follows that turns can be short-circuited by the contact. To prevent damage to the winding, the contact is made of resistive material that limits the circulating current in the short-circuited turn. Since the load current has to flow through the contact, the resistance is kept as low as possible. The problem encountered is very similar to the problem of commutation in generators or motors, and the materials used for the contact are the same that are used in the familiar carbon brushes of rotating machines. The Variac transformer is designed to stand indefinitely the additional heat produced in the short-circuited turn and no attempt is made to prevent the contact from resting between turns. On the contrary, short-circuiting turns is one of the important advantages of the Variac, as it affords a gradual change from one tap to the other. Ordinarily, the voltage-per-turn is between one-half volt and one volt.

Uses

The advantages of the variable-ratio autotransformer over rheostats and other kinds of voltage-control devices are high efficiency, good regulation, and linear smooth control from zero to above line voltage. Mechanically the transformer is small in size and simple in construction.

In industrial control and in experimental problems in the laboratory, Variacs are used wherever alternating voltages must be adjusted, and by means of voltages, other quantities such as speed, heat, or light. Typical applications are speed control of motors, heat control of electric furnaces or heaters in a laboratory, illumination control in small theaters or dark rooms, output voltage control of high-voltage transformers and rectifier systems, filament control for radio-transmitter tubes, and voltage control for electrical or magnetic testing, calibration, and measurement.

Contact Problems

As mentioned before, the transformer uses a carbon brush, called the contact, to connect the load to the individual turns of the winding. Resistance characteristics, mechanical design, and thermal properties of the contact are most important for proper operation of the transformer. A schematic drawing of the Variac together with a detailed view of the contact is shown in Figure 4. The width of the contact is such that in sliding over the winding, one turn can be short-circuited but not two. The contact is represented as a block, connecting points 1 and 2 on successive turns of the winding to point 3 on the contact holder. The three-terminal block can be represented by an equivalent circuit of three resistors, R_1, R_2, R_3, between the points 1, 2, and 3 as shown in Figure 5. This equivalent circuit has been found useful in the following analysis of optimum contact design, but it should be understood that the contact resistance between a carbon brush and a metallic surface varies with the current passing through the surface and that the resistance of the carbon brush itself depends to a large degree on current density. Although the behavior of carbon brushes is not very well understood, they have been used for many purposes with great success for a long time and can be manufactured to close specifications. Experimental data show that the resistance of a carbon brush varies with the current passing through it in a way to maintain a substantially constant voltage drop. This voltage drop varies with the composition of the carbon and with the pressure applied and is used by brush manufacturers in the rating of their product. A typical current-voltage characteristic of a carbon brush is shown in Figure 6. It is reasonable to expect that a definite relation between this voltage drop and the voltage-per-turn of the Variac transformer has to be maintained to prevent excessive heating of the short-circuited turn and of the contact.

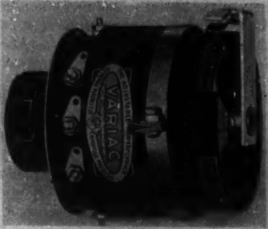
Figure 1. 170-volt-ampere Variac transformer assembled for back-of-panel mounting

Figure 2. An 860-volt-ampere Variac transformer for laboratory use

Figure 3. A five-kilovolt ampere Variac transformer

To show this relation, it is convenient to use the equivalent circuit shown in Figure 5. It can be seen that resistance R_3 is not included in the circuit of the short-circuited turn. Since it does not serve any useful purpose, it can be reduced to a small value by proper design of the contact holder and could be considered as part of the load resistance R_5. The resistance of one turn of the winding, R_4 is made as low as possible in accordance with transformer design practice. The resistances R_1 and R_2, therefore, have to be relied on to keep the circulating current in the short-circuited turn below a value that might damage the transformer or overheat the contact. This circulating current is shown as I_0 in Figure 5 and the load current as I_2. The voltage-per-turn between points 1 and 2 is e_0. To simplify the following analysis of optimum contact design, Figure 5 is drawn as a section of a two-circuit transformer rather than of an autotransformer, and the current in the winding preceding the short-circuited turn is the same as the load current. It is further assumed that the contact is symmetrically located with respect to the two points 1 and 2, but R_1 is not necessarily the same as R_2, since the current flowing through these two points will not be the same. In an autotransformer the current in the winding would be different from the load current, but the contact problem would be exactly the same.

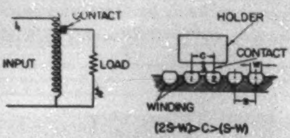

Figure 4. Schematic diagram of Variac transformer and contact

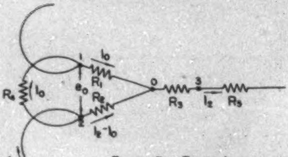

Figure 5. Equivalent circuit of short-circuited turn and contact

The problem of optimum contact design for a transformer of a given voltage-per-turn e_0 and given maximum load current I_2 is reduced to the problem now, of finding those values of R_1 and R_2 for which the energy loss is a minimum. It must be remembered, of course, that both R_1 and R_2 are the equivalent resistances of a single piece of carbon material and that they cannot be chosen independently of each other. The obvious suggestion, therefore, of making R_1 zero and R_2 infinite cannot be realized in a Variac transformer. It is a perfectly good solution and is used in tap-changing transformers of conventional design.

The energy loss in the two resistances according to Figure 2 is

$$W=R_1I_0^2+R_2(I_2-I_0)^2 \tag{1}$$

and since

$$R_1I_0=R_2(I_2-I_0)+e_0 \tag{2}$$

$$W=\frac{e_0^2}{R_1+R_2}+I_2^2\,\frac{R_1R_2}{R_1+R_2} \tag{3}$$

The first term is seen to be the dissipation due to the voltage between adjacent turns acting across the two brush resistances in series. The second term is due to the load current flowing through the two resistances in parallel. The resistances R_1 and R_2 correspond to the currents I_0 and I_2-I_0. If these resistances were constant and independent of current, the first term would be the no-load brush dissipation. If the ratio of the two resistances is called n^2

$$R_2=n^2R_1 \tag{4}$$

and substitution of equation 4 in equation 3 yields

$$W=\frac{e_0^2}{(1+n^2)R_1}+I_2^2R_1\,\frac{n^2}{1+n^2} \tag{5}$$

Setting the derivative of W with respect to R_1 in equation 5 to zero, the value of R_1 is obtained for which the contact loss is a minimum.

$$R_1=\frac{e_0}{I_2n} \tag{6}$$

The combination of equations 4 and 6 gives a resistance value R_0 which might be called the optimum contact resistance.

$$R_0=\sqrt{R_1R_2}=\frac{e_0}{I_2} \tag{7}$$

If the two brush resistances are equal, they should have the value R_0 for minimum total loss. If they are unequal, their geometric mean should have that value.

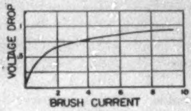

Figure 6. Current–voltage characteristic of a carbon contact

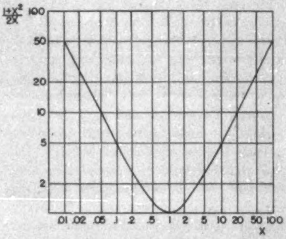

Figure 7. Plot of a function similar to that used in equation 11

To find out how much the contact loss is increased if the geometric mean of the two resistances deviates from R_0, a second parameter k is introduced

$$\sqrt{R_1R_2}=nR_1=k\frac{e_0}{I_2} \tag{8}$$

and by substituting equation 8 in equation 5, the energy loss is found to be

$$W=e_0I_2\,\frac{n}{1+n^2}\cdot\frac{1+k^2}{k} \tag{9}$$

If the two resistances R_1 and R_2 are equal and have the optimum value, both n and k are 1 and the energy loss may be called W_0.

$$W_0=e_0I_2 \tag{10}$$

It follows that in the general case, if R_1 and R_2 have arbitrary values

$$W=W_0\,\frac{2n}{1+n^2}\,\frac{1+k^2}{2k} \tag{11}$$

Figure 7 shows the plot of a function similar to that used in equation 11 and it can be seen that the energy loss is increased by 25 per cent, for instance, if the geometric mean of the two brush resistances is double or is one-half of the optimum value, and that this increase of loss can be compensated by making one of the resistances four times as large as the other. It is apparent that to reduce the energy loss considerably below W_0, very high ratios of brush resistance values are required. The two factors in equation 11 tend to offset each other and consequently the geometric mean resistance must be held closely to the correct value.

Returning now again to Figure 5, it can be deduced without any computation that the energy loss $W_0=e_0I_2$ is obtained, if the voltage across R_1 equals e_0. In that case, the voltage across R_2 is zero. The full-load current I_2 flows through R_1 and the current in the short-circuited turn equals the load current. This condition is expressed by

$$I_0=I_2 \tag{12}$$

$$R_1I_2=e_0 \tag{13}$$

Substituting equation 13 in equation 3, it is seen that

$$W=e_0I_2 \tag{14}$$

independent of R_2.

That a relation of this kind between the voltage per turn of a Variac transformer and the voltage drop in the contact would have to be maintained was suggested at the beginning of this analysis. It is apparent now that if the voltage drop in the contact equals the voltage per turn, the contact resistance is the optimum value according to equation 7, and the contact loss equals load current times voltage per turn.

Although it is possible, according to equation 11, further to decrease the contact loss if the two contact resistances R_1 and R_2 could be chosen at will, and could be maintained in the proper relation as the contact moves from one turn to the other, it has been found in practice that materials with the required characteristic are not available. As a matter of fact, it is often difficult to find a material that will fit the simple relation of equation 13 where resistance or voltage drop is specified for a single value of current.

If the maximum load current of a particular transformer is used to determine the optimum contact resistance in accordance with equation 13, the contact loss will be smaller than W from equation 14 for any other load current, and will be less than one-half this value for no-load condition. Under no load the short-circuit current must flow through R_1 and R_2 in series, and each one will be larger than R_1 from equation 13. This follows from the current-voltage characteristic of the material used, which will always have the general shape shown in Figure 6. The no-load short-circuit current, therefore, will be less than one-half the maximum load current.

It has been shown in the preceding paragraphs that the contact loss is proportional to the load current and to the voltage-per-turn. If the voltage-per-turn were increased, as is usual in larger transformers, the contact loss would rise rapidly and would soon present a serious problem. Decreasing the number of turns below a definite value is undesirable for another reason. If a 200-turn Variac is adjusted for 50 per cent output voltage for instance, the voltage-per-turn at that particular point is 1 per cent of output voltage, but in a 100-turn Variac at 10 per cent output voltage, it is 10 per cent. It is true that the resistance of the Variac contact helps to smooth out the steps between turns, but it has been found in practice that 100 to 200 turns should be used, even in large transformers, to maintain smooth control at low output voltages.

At 100 turns the contact loss of a Variac transformer is 1 per cent of output power, according to equation 14. Iron and copper losses of the same transformer might be an additional 3 per cent. If operation of the transformer at rated output produces the maximum allowable temperature rise of winding and core, about an additional one third of the winding and core surface is needed to take care of the contact loss. The contact loss is concentrated in the small contact block and good heat conduction between the contact block and the cooling surface is required. The cooling surface is called the radiator.

Contact Design

Actual design of contact and radiator are closely dependent on the design of core and winding, but the basic requirements can be stated independently.

The active surface of the contact should be as large as possible to reduce current density and to spread the inevitable contact loss over the largest possible area. The width of the contact, shown as *C* in Figure 4, is usually very small, since it is determined by the cross section of the turns. The other dimension of the contact surface is related to the cross section of the core and depends on the length of turn available in the plane that has been chosen for operation of the contact. Figure 8 shows three different core cross sections. Core "A" would be best for a contact operating on top, and core "C" for a contact on the circumference. Small transformers ordinarily have banked windings. A core of shape "A," for instance, would be made with the outside diameter three times as large as the inside diameter. The winding would be applied to form a single closely spaced layer of turns on the outside, and the turns would pile up in three layers on the inside. The top surface, then, would not be available for operation of the contact, except at the outside edge.

Carbon brushes used on rotating machines are subjected to considerable wear and are made long, to avoid frequent replacements. Variac contacts show very little wear and are made as short as possible, to reduce losses and to afford better heat conduction into the radiator. In laboratory tests, contacts show extremely small wear in continuous operation. The wear seems to be independent of load, and is about 0.02 inch in one million adjustment cycles. In actual use, contacts wear more rapidly than this figure would indicate, and it appears that resistance to wear is decreased if the contact is allowed to rest under load in the same position for long periods of time. In any case, Variac brushes do not "wear in" after a few days of operation like brushes of rotating machines. Their performance can, therefore, be made more uniform and more reliable if they are subdivided into several independent units. Large Variacs are built with six or more contacts operating in parallel, but on small transformers, this added complication is not always justified.

Since Variac brushes do not wear in, current density and specific pressure vary within wide limits, and can hardly be compared with the values recommended by carbon manufacturers. Current carrying capacity and pressure of a given contact are best determined in statistical tests on the winding surface of the transformer.

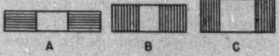

Figure 8. Cross section of cores

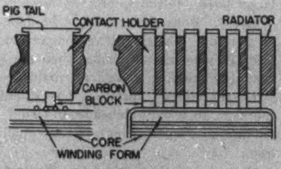

Figure 9. Six-brush contact operating on top of transformer

As an example, a 115 volt 5 kva transformer with 160 turns uses six contacts in parallel, each one carrying from five to ten amperes and having a spring pressure of about ten ounces. The dimensions of each one of the six blocks are approximately one quarter inch by one quarter inch by one quarter inch. These small carbon blocks are permanently fastened by soldering, or by pressure fit, into larger contact holders and form together with them six complete brushes. The brushes, in turn, are inserted into six slots of the radiator, and are pressed against the winding by springs. A schematic view of a six-brush contact is shown in Figure 9. The advantage of this brush construction, compared to the large carbon blocks used in motors and generators, is improved heat conduction to the radiator. As a result, the temperature of the contact surface can be kept at a lower value, and damage of the winding insulation is prevented. The radiator absorbs the heat of the contact primarily by conduction and transmits it to the surrounding air by radiation and convection. The material of the radiator is chosen for good heat conduction. Size and surface treatment determine radiation. Convection currents depend on general shape and are improved by ventilating channels.

To prevent the metal contact holders from short-circuiting the winding, if a carbon contact block should wear or break by accident, the travel of the brushes within the radiator slots is limited. Electrical connections to the brushes are made by flexible leads.

Contacts for Low-Power Transformers

The discussion of contacts, so far, has been mostly concerned with large transformers where contact losses must be reduced to a minimum. If contact losses rise rapidly with output power, it is equally true that they decrease on small transformers. On a 200-volt-ampere Variac transformer, for instance, with 500 turns, wound on a three inch-diameter core, contact losses according to equation 14 are 0.4 watt. This small power does not present any particular problem but the width of the contact would have to be under 0.025 inch to make it impossible to short-circuit more than one turn at a time. A carbon contact of that size would not be strong enough, but since the contact loss is low, no harm is done by

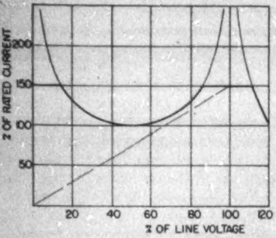

Figure 10. Output current for constant copper loss

increasing the width of the contact and letting it short-circuit several turns at a time. The transformer mentioned above actually uses a round contact of 0.09 inch diameter. It appears that six wires could be touched by a contact of this size, but because of the round shape, only three turns are effectively short-circuited and the contact loss is increased to 1.2 watts. It can be said in general that contact design need not follow the procedure outlined in the two preceding paragraphs unless it is desirable to limit contact losses to the inevitable minimum.

Winding and Core

A Variac transformer differs very markedly from a conventional transformer of equal rating. This is mainly due to four requirements discussed in detail in the preceding paragraphs: (1) the core is toroidally shaped, (2) the winding has to be applied to form a single layer at the contact surface, (3) the number of turns cannot be chosen for best efficiency or economy alone, and (4) the contact loss has to be added to the losses in copper and iron. If these points are kept in mind, winding and core can be designed by applying standard practice. In the following paragraphs, therefore, only a few points will be mentioned that are characteristic of the Variac.

A single-layer winding on a toroidal core does not fill up the winding space, and it is obvious that the amount of copper used is out of proportion to the amount of iron. This is particularly true with round wires and small toroids where the outside diameter must be much larger than the inside diameter. It is possible, of course, to use wire with rectangular cross section and to use large toroids, but that cannot be done when a compact transformer of low output power is desired. Rectangular wire could be used to great advantage in large transformers, over two kva for instance, but so far, the mechanical problem of applying the winding over the toroid has not been solved.

The resistance of a single-layer winding of round wire increases with the third power of the number of turns, and it follows, that the power rating of a toroidal core depends upon the voltage applied. For this reason Variac transformers cannot be built economically for high voltages.

The rating of a conventional transformer core increases rapidly with the frequency applied, since the number of turns can be decreased. In a Variac transformer, the number of turns is determined by the voltage applied and the only benefit obtained at increased frequencies is a reduction of core loss. However, if the cross section of the core is reduced at high frequencies, the gain is in the same proportion as in any transformer.

A laminated toroidally shaped core can be formed by two methods which are indicated in Figure 8. Core *A* is obtained by stacking ring-shaped laminations, core *B* by winding a long strip over a mandrel. Losses in core *B* will be slightly lower than losses in core *A*, but both methods are equally satisfactory, and the choice depends mainly on the availability of materials and tools.

Application of a winding over a toroidal core requires special equipment, unless it is done by hand. Hand-winding, however, is not very practical, since small transformers have many hundreds of turns, and large transformers require wires that are too hard to handle. Toroid-winding machines[2] are ingeniously designed and apply single-layer or banked windings on large and small cores. They can handle a very wide range of wire sizes. It takes about 5 minutes to wind 500 turns of number 28 wire on a 3-inch-diameter core, and 25 minutes to wind 160 turns of number 8 wire on a 12-inch-diameter core.

The wire used is always insulated. A very high grade of insulation is required to stand the mechanical abuse during the process of winding, and to stand high temperatures in operation. At the contact surface, the insulation has to be removed. In addition, the wire is flattened to offer a larger surface to the contact, as shown in Figure 4. Both operations can be done on a grinding wheel. It is very important that the wires cannot change their position after grinding, since that would destroy the contact surface. The wires, therefore, are usually embedded in a winding form with a groove for each individual turn.

Mounting of the toroidal core, covered almost entirely by the winding, is another problem that requires serious consideration. Ordinarily, the winding covers 320 degrees and clamps cannot be used where they would interfere with the movement of the contact, or where they would form a short-circuited turn around the core. Permanent close alignment of the control shaft and the contact surface is very important, and high local pressure must be avoided on the winding.

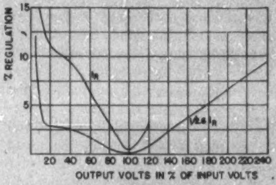

Figure 11. Regulation of transformer with 1.2 and 2.4 voltage step-up

Most Variac transformers are designed for air-cooling, but forced air draft and oil have been used in special applications to reduce size and weight to a minimum.

Rating and Taps

In the discussion of contact loss the term "maximum load current" has been used. It was assumed that the transformer would reach the highest permitted temperature at this current, the largest current that the contact has to handle. This current, called the "rated current," is an important characteristic of the Variac transformer.

Figure 4 shows the most commonly used connection of a Variac transformer to line and load. If the output voltage into the load is changed by moving the contact, and the load current is held constant by changing the load resistance, the current in the different parts of the winding changes. Copper losses, except those due to magnetizing current in the winding, start at zero for zero output voltage, reach a maximum at one-half line voltage, are zero at line voltage, and increase rapidly from there on. Core and contact losses are independent of output voltage.

The point above line voltage, for which the losses equal again the losses at half line voltage, is found to be at 20 per cent above line voltage. For this reason the line voltage tap on Variac transformers is almost always at 83 per cent of the turns. At rated current, the temperature rise of the Variac transformer reaches the maximum value at two settings of output voltage: at one-half line voltage and at maximum. The rated current could be exceeded at all other points if it were not limited by the contact. In Figure 10 load current in per cent of rated current, for constant copper loss, is plotted against output voltage, in per cent of line voltage. If the contact is designed for a "maximum current," 50 per cent over "rated current," load currents up to the heavy line in Figure 10 can be used. The dotted line is the voltage-current characteristic of a constant load that draws "maximum current" at line voltage. It can be seen that this straight line is always below the limiting curve, and it follows that the transformer would not overheat at any output voltage, under line-voltage, if connected to this load. Experience has shown that contacts can be designed to carry considerably more than rated current in small Variac transformers, but in large transformers, the contact surface is not always sufficient to allow an increase above rated current as high as 50 per cent. Variac transformers are, therefore, rated by the following terms: (1) rated current—the current that can be drawn at any position of the control shaft; (2) maximum current—the current that can be drawn at low output voltages and at voltages near input voltage; (3) load rating—the volt-amperes obtained by multiplying maximum current by line voltage.

Whenever output voltages above line voltage are not required, or when the load could be damaged by overvoltage, the line should not be connected to the tap of the transformer, but to the full winding. In other applications it may be desirable to obtain more than a 20 per cent increase of output voltage with respect to line voltage, and the input could be connected still further down on the winding. The voltage across the full winding cannot be increased, of course, since that would increase flux density and voltage-per-turn, but all Variac transformers designed for 230-volt lines are tapped for connection to 115-volt circuits. If power is supplied at rated current to the center tap of a transformer, the output current is one half of rated current. If power is supplied to a tap that will give a maximum output voltage of 2×1.2=2.4 times input voltage, the connection provided on 230-volt Variac transformers, the output current for which copper losses are not excessive is 1/2.6 of rated current.

Regulation

Regulation of a transformer can be expressed as a voltage drop under load, in per cent of output voltage. In a Variac transformer the voltage drop in the contact is added to the voltage drop in the winding, and since the contact drop at full load current always equals the voltage-per-turn, the regulation at low output voltages is considerably increased. On the tenth turn of a transformer, the regulation due to contact drop always must be 10 per cent under these conditions, but the actual voltage of the tenth turn decreases as the total number of turns is increased. In a 200-turn Variac, connected to give 20 per cent overvoltage, for instance, the voltage-per-turn is only 0.6 per cent of input voltage, and contact regulation is better than 10 per cent, down to output voltages as low as six per cent of line voltage.

Autotransformers have inherently better regulation than two-circuit transformers. The improvement is most noticeable if the output voltage is close to line voltage and regulation is so good that careful protection against short circuits in the load is important. On the other hand, the leakage reactance of a single-layer winding on a toroidal core is large compared to the leakage reactance of a conventional transformer where it can be minimized by proper design.

Over-all regulation of Variac transformers in their most useful range, near input voltage, can be considered excellent compared to other control devices, but it should be kept in mind that at low output voltages and at high ratios of voltage step-up, voltage drop under load cannot be disregarded. A typical regulation curve is shown in Figure 11, where voltage drop in per cent of output voltage is plotted against output voltage in per cent of input voltage. The upper curve applies for a Variac transformer with a maximum voltage step-up of 1.2 at rated current and the lower curve for a voltage step-up of 2.4 at 1/2.6 of rated current. The effect of contact drop can be clearly seen in these curves at low output voltage. The inversion of the curves at one third of input voltage is due to the fact that output power decreases at lower output voltages. A further discussion of regulation is beyond the scope of this paper, but it is interesting to note that for constant voltage-per-turn the regulation at rated current varies very little with the size of the transformer.

Special Transformers

The use of a carbon composition contact on the turns of a transformer winding is not limited to toroidal autotransformers. This particular application has been discussed in detail, since it combines many advantages, but it is obvious that the transformer core could have any desired shape and that it could carry any number of additional windings.

One important application of a Variac transformer with an additional winding is a voltage regulator with limited range, that can vary the output voltage from 100 to 130 volts, for instance, when connected to a 115-volt line, or maintain constant 115-volt output voltage if the line voltage varies from 90 to 120 volts.

In another application, line-voltage is connected to the contact of a multiple-winding transformer. Moving the contact changes the number of primary turns and varies simultaneously the output voltage of several windings.

For applications in multiple-phase circuits two or more Variac transformers are mounted together with their contacts controlled by a single shaft.

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2. TOROIDAL WINDING MACHINES, F. E. Planer. *Journal of Scientific Instruments*, Institute of Physics, London, volume 20, December 1943, pages 185–90.

Prevention of Rotor-Winding Deformation on Turbogenerators

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Synopsis: During recent years there have occurred several cases of failures on turbogenerators which were found to be due to a singular type of deformation of the rotor windings. It is recognized that the deformation of rotor windings is due to forces set up in the windings by the interaction of centrifugal force and temperature change.[1]

The purpose of this paper is to discuss briefly the phenomena responsible for this type of deformation, and to show that by means of preheating of the rotor windings to a definite predeterminable winding temperature, the resulting mechanical stresses can be very materially reduced so that winding deformation is held to a minimum.

Cause of Rotor-Winding Deformation

THE phenomenon of rotor winding deformation on turbogenerator rotors is discussed in reference 1. As copies of this paper are not readily available, the subject is here briefly reviewed.

All large turbogenerator rotors are built with the rotor winding, consisting of a number of multiturn coils laid in milled radial slots with metal wedges supporting the coils against the radially directed centrifugal force. Asbestos and mica are the principal insulating materials. At the ends of the rotor body, the individual coils are supported against deformation by end rings, with insulating material bracing blocks. The mechanical strength that can be built into the bracing is limited by the mechanical strength of the insulating materials used for coil insulation.

Several cases of rotor winding failure have occurred that were found to be due to a peculiar type of deformation of the rotor coils. This deformation is the result of shortening of the rotor conductors within the rotor slots. The failures, involving electrical grounds and interturn short circuits, all occurred at points where the rotor windings leave the rotor body or in the portions of the coils located under the end bells of the rotor. Grounds developed by crushing of the coil insulation, whereas interturn short circuits were found to be caused by the crushing of the turn-to-turn insulation. In each of these cases the underlying cause was a deformation of the coils, in which the conductors nearest to the center of the rotor shaft had become shortened most, while conductors more remote from the rotor shaft showed diminished shortening; the outermost conductors being found entirely unaffected. Blocking between coils was found either bent or broken, showing that the forces involved were high in order of magnitude. In some cases, the innermost conductors were found displaced from their original position by as much as the width of the conductors. These displaced conductors, under the influence of centrifugal force, were subjected to eccentric loading between conductors and the resulting high unit pressures on the turn-to-turn insulation ultimately produced structural failure of this insulation, causing turn-to-turn short circuits. Figures 1A, 1B, and 1C illustrate the conditions.

The basic cause of rotor-winding deformation may be explained if the processes of starting, loading, and shutting down of the turbogenerator are examined in detail. The turbogenerator, having cooled during a preceding outage to approximately ambient temperature, is started via the turbine and is driven at low speed for a period varying from one half to two hours, depending on the size of the unit involved and on the temperature the turbine has cooled to. Near the end of the turbine "warming up" period, the generator speed is rapidly increased to synchronous speed and, after applying field excitation, the generator is synchronized to the bus and loaded. The significant point of this starting procedure is that field current is not applied to the rotor winding until after nearly all of the centrifugal force is established by the rotational speed and the conductors are locked in position as a result. The rotor winding is therefore not subjected to a temperature rise until after the full effect of centrifugal force is established. After the field current is applied and as the generator is loaded, the rotor winding resistance loss appears as heat in the rotor conductors. As the temperature of the conductors increases a portion of this heat, proportional to the time rate of temperature rise, is stored in the thermal capacitance of the conductors and the remainder is transferred by conduction through the coil insulation to the rotor body at a rate proportional to the instantaneous temperature difference between the rotor winding and the rotor body. This process of combined heat storage and heat dissipation by conduction, results in an exponential approach to an ultimate temperature and involves a thermal time constant (Appendix IV). As the total weight, and with it the thermal mass, of the rotor winding is small, the thermal time constant of rotor-winding temperature rise over the rotor-body temperature is small. As heat is transferred to the rotor body, it experiences a temperature rise over the surrounding air. During increasing rotor-body temperature a portion of the heat transferred to it from the rotor winding is again stored (proportional to the rate of temperature rise) and the remainder is transferred to the surrounding air-gap air by forced convection at a rate proportional to the temperature difference. Because of the great weight of the rotor body, its thermal time constant is large (Appendix IV). With suddenly applied and thereafter constant field current the process of heat dissipation from the rotor winding is summarized as follows: The temperature rise of the rotor body over air-gap air (or approximately ambient air) reaches its final value slowly. After the initial fast temperature rise of the rotor winding, the winding continues to increase in temperature as the rotor body heats, finally maintaining, for constant field current, a constant differential.

While these temperature changes occur, the rotor conductors are under the influence of centrifugal force which tends to force them radially outward. Each rotor-conductor's centrifugal force is a function of its radial location and angular velocity. The centrifugal force of the innermost conductor is imposed on the next conductor, whose own centrifugal force adds to that of the first conductor, imposing the total on the third conductor and so on, until the wedge supports the accumulated centrifugal force of all conductors in the coil. Figure 2 shows a typical curve for a large rotor, giving centrifugal forces in terms of conductor location.

Any tendency of the coil as a whole, or of any conductor in the coil, to move in an axial direction is opposed by the frictional resistance created by the centrifugal force. The magnitude of the coefficient of friction is somewhat uncertain but probably

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lies between 0.1 and 0.3. Even with the lower value and with values of centrifugal force as given in Figure 2, it is obvious that large portions of the slot lengths of the rotor conductor are firmly locked on each other and on to the wedge.

However, as the rotor conductors are being heated they tend to expand against the frictional forces which hold them locked in place. When the conductor-temperature rise is low, the tendency of the conductors to expand results in the creation of compressive stresses in the conductor within the elastic limit of the conductor material. With increasing rotor-body temperature, additional stress is produced on account of the differences in the coefficients of linear expansion of rotor conductor and rotor body, which at some critical temperature rise produce total stresses in the rotor conductor in excess of the elastic limit, and result thereafter in permanent plastic deformation. When the generator's load is reduced, the temperature of the rotor winding decreases, and the initial stress, which is held in elastic deformation, is released. When the generator is shut down, cooling takes place without the restraint of centrifugal force and the rotor conductors are free to contract as they cool. The plastic strain which was initially established at the high temperatures is retained, leaving the so-strained conductors permanently shortened. Repetition of this cycle of starting, loading, and shutting down at sufficiently high temperature rises results in continuing deformation in spite of the hardening effect of the cyclic plastic working of the conductor material. At some lower temperature rises however, deformation may cease after a small number of cycles because of the hardening effect of plastic working during the first few cycles.

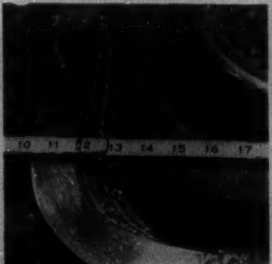

Figure 1A. Rotor-winding distortion, 160,000-kva 1,500-rpm generator

The preceding analysis gives a satisfactory qualitative exposition of the phenomenon of winding deformation for which a quantitative verification is more complex. The added complexity comes from the fact that in the discussion of conductor temperature an assumption of equal temperature for all conductors of a coil is really inadmissible. The temperature of the conductors of a coil is lowest for the outermost conductor, it rises to a maximum somewhere in the interior of the coil, and drops again toward the innermost conductor. Figure 3 shows a relation of conductor location and temperature rise, calculated by means of an equivalent electrical network for a rotor coil of a large generator neglecting all heat flow in the axial direction. Based on the temperature rise distribution of Figure 3, on an idealized stress-strain curve, as shown in Figure 4, and in consideration of the effects of varying centrifugal force with conductor location, the deformation for a single cycle, Figure 5, was calculated. Comparing calculated deformation (solid line in Figure 5, with the measured deformation on the generator rotor shown in Figure 1A (dashed line in Figure 5) good agreement may be observed to exist for all conductors excepting the two innermost ones. Similarly, calculated curves, shown as Figure 8 in reference 1, show a like tendency toward decreasing deformation for the innermost conductors. It appears, therefore, that in addition to the previously discussed factors, there must be others in order to account for the remaining discrepancy between calculated and observed conditions. One of these factors is probably buckling of the innermost conductors in whatever space there may exist at the bottom of the slot. The force required to buckle a conductor as a column may be expected to be relatively small since a conductor, as a column, is a very weak one indeed, even if the radial bracing by centrifugal force is taken into account.

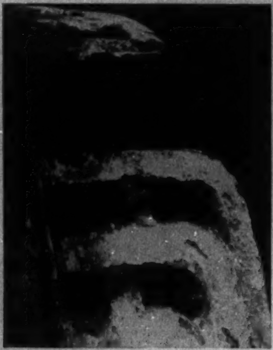

Figure 1B. Rotor-winding distortion, 21,000-kva 1,500-rpm generator

Prevention of Rotor-Winding Deformation By Preheating

In the preceding section it was shown that the rotor-winding deformation is caused by the temperature change which occurs after the rotor conductors are prevented from free expansion by the locking action of centrifugal force. It was shown that when the temperature changes are sufficiently small, deformation is small and moreover may be expected to cease entirely after a limited number of cycles. The prevention of excessive rotor-winding deformation, therefore, lies in the reduction of the magnitude of temperature variations. The permissible magnitude of temperature variation is a function of the mechanical properties of the conductor material and the generator designer has control of both the magnitude of temperature variation and the conductor material used. However, the operator gains control of one of the factors involving the magnitude of stress developed in the conductor, when he is enabled to fix the initial temperature from which all subsequent temperature changes take place. By preheating the rotor winding and body with the body revolving at very low speed the expansion of the conductors and rotor body, as the temperature rises from ambient to preheat temperature, takes place free from frictional restraint produced by centrifugal force. Subsequent temperature variations due to generator load changes at synchronous speed then produce stresses

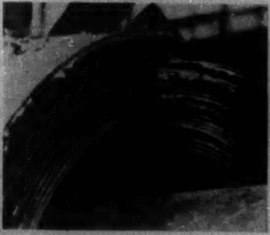

Figure 1C. Rotor-winding distortion caused by overloading 50,000-kva 50-cycle generator

which are proportional to the difference between actual and preheat temperature instead of the difference between actual and ambient temperature. Furthermore, the initial or preheat temperature can be chosen in such a manner that the stress in the conductor material becomes a tensile stress at light loads and the compressive stress which would result in creep in compression at high loads is nullified by the creep in tension at light loads. That initial or preheat winding temperature which produces a minimum of permanent winding deformation for a given rotor during the expected operating cycle is the optimum preheat temperature for the given generator.

As the expansion of the rotor body is also a factor contributing to the total stress in the winding conductors, there is also an optimum preheat temperature for the rotor body, which, together with the optimum winding preheat temperature, assures lowest possible deformation. As the rotor winding temperature and the rotor body temperature differ by the temperature rise of the winding over the rotor body, it is to be expected that the two optimum temperatures, that of the rotor body and that of the rotor winding, will be found to differ. This situation points to preheating with current in the rotor winding as the best method and knowing the difference between the two optimum temperatures permits the determination of the required current.

In the determination of the optimum preheat temperatures two criteria exist, namely, minimum stress and minimum deformation. The two are not synonymous, since the latter is not only a function of stress but of the duration of stress as well. An example of a calculation for a preheat procedure is given in Appendix II of this paper in which equations having the criteria of minimum deformation, developed in Appendix III are used. The development of the equations of Appendix III are based upon an assumed behavior of copper under mechanical stress which is believed to be approximate to the actual behavior of copper in the temperature range and stress range as here applied, with sufficient accuracy for the purpose. Because of the necessary approximation it is desirable to test the result of calculation made with the method of Appendix III, by calculating expected extreme stresses as is done in the example of Appendix II. For most load cycles, the method of Appendix III will give tolerable values of stress. In the author's opinion tolerable values of stress for windings of soft annealed copper are in the order of those calculated in Appendix II, and should not exceed 5,000 pounds per square inch.

The refinement that can reasonably be incorporated in a preheating procedure is limited by practical considerations, of which a primary one is the accuracy with which rotor-winding and rotor-body temperatures can be determined. Experience shows that this accuracy is not likely to be greater than plus or minus five degrees centigrade. The assumptions in regard to the behavior of the conductor material, made in Appendix III must be viewed in this light, and when so viewed they become more acceptable.

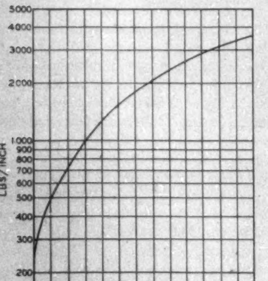

Figure 2. Centrifugal force on field-winding conductors at rated speed

Practical Aspects of Preheating

In the practical application of preheating of turbogenerator rotors, there are a number of factors, aside from those previously discussed, that must be considered if a workable procedure is to be obtained. Among these are:

(*a*). The arrangement of existing cooling system, that is, whether the generator is equipped with shaft driven blowers or with separately driven blowers, and whether the generator cooling system is a closed or open system.

(*b*). What means are available to furnish the required preheating current.

(*c*). There is the problem of co-ordinating the preheating procedure with the starting procedure of the turbine.

In regard to the existing cooling system, the preceding discussions show that it is desirable to obtain by means of preheating a certain definite rotor-winding temperature and also a temperature distribution throughout the winding as near as possible to that existing when the generator is in operation and carrying load. This requirement can be approached only if preheating is carried out with as near as possible normal air flow through the generator; however, it cannot be reached completely since preheating must be performed with the rotor revolving at low speed. On generators equipped with shaft-driven blowers, the requirements of nearly normal ventilation and low generator speed, of course, cannot be met simultaneously. In this case it may be necessary to use a procedure in which preheating is carried out in steps. This is accomplished by starting the preheat at relatively high rotor speeds in order to provide the required ventilation and then at one or more intermediate temperatures reducing the speed, the steps being so timed as to prevent excessive winding stresses. With each step of speed reduction the accumulated stresses are released until, at the point where the specified preheating temperature is reached, a final slowing down releases all stresses before the generator is again accelerated and synchronized. On generators equipped with separate blowers, this difficulty is not likely to appear.

The nature of the cooling system is a factor in the preheating procedure because of the necessity to measure the rotor body temperature indirectly, that is, in terms of the winding temperature. This involves the thermal time constant of the rotor-winding-temperature rise over the rotor-body temperature. In view of the relative smallness of this time constant, it is convenient to eliminate it as a factor by holding the preheating current constant at that value which produces the expected rotor-winding-over-rotor-body temperature rise. However, with constant preheating current, the control of temperature lies in the adjustment of the cooling

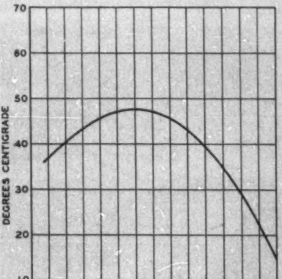

Figure 3. Temperature rise distribution in field coil

system. This is an easy matter on generators having a closed cooling system in which case the water flow through the coolers can be regulated. Where a generator operates on an open ventilating system the considerations of the preceding paragraphs in regard to temperature distribution within the rotor winding may have to be compromised because of the necessity of controlling the winding temperature by changes in turbine speed. Variations in the preheating current for the control of rotor-winding temperature should be resorted to only after all other means of controlling temperature are exhausted, since the attainment of the optimum rotor winding—rotor body temperature rise then becomes very difficult.

In regard to the availability of direct current for preheating, several additional factors enter into consideration. When a motor-driven or turbine-driven d-c variable-voltage generator of sufficient capacity is available, the problem is simple. Where the preheating current must be taken from a constant-voltage d-c source and where a field rheostat in the rotor circuit exists, the problem is equally simple. When neither exists, preheating can be carried on in steps at generator speeds sufficiently high to permit the use of the shaft exciter as power source. In the latter case, it may be found expedient to provide space heaters in the generator for the purpose of holding the generator at some elevated temperature from which preheating can be carried out in one step. Finally, the objective of preheating can be approached in some cases to a sufficient extent by the use of space heaters alone. The latter two methods may be satisfactorily applied in those cases where generators are taken off the line only infrequently so that higher stresses and larger values of deformation per cycle become acceptable.

The problem of co-ordinating a particular preheating procedure with the turbine starting operations contains so many factors in addition to those previously discussed, that an attempt to generalize is practically of no value.

Experience With Preheating

Preheating of rotor windings has been adopted for five generators on the system of the Consolidated Edison Company of New York, Inc., and the experience at the time of preparation of this paper extends over a period of approximately six months. The generators involved are turbogenerators of the following sizes: two generators, 200,000 kva, 60 cycles, 0.8 power factor, 1,800 rpm; one generator, 160,000 kva 25 cycles, 1.0 power factor, 1,500 rpm; and two generators, 60,000 kva, 25 cycles, 1.0 power factor, 1,500 rpm. Appendix I gives detailed data for the 160,000-kva, 25-cycle generator to which the example of Appendix II applies.

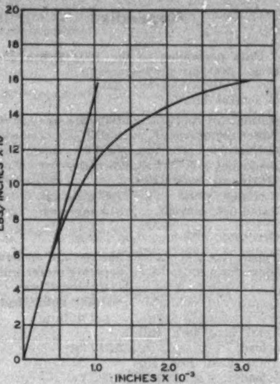

Figure 4. Stress-strain curve for soft annealed copper

All these generators are equipped with external motor- or turbine-driven blowers, with closed cooling systems using air coolers, with shaft rotating gear and with field rheostats in the rotor circuit. Aside from differences in specified preheating currents and temperatures, the preheating procedures for all of these generators are substantially the same. Subsequent discussion will, therefore, be confined to the 160,000-kva 25-cycle unit.

The turbine starting operation and preheating procedure are carried out as follows:

1. Depending upon the duration of the preceding outage of the turbogenerator, that is, upon the initial temperature of the generator rotor, the time required for preheating is varied from one to two and one half hours. Generally, the "warming up" time of the turbine varies similarly, but it is possible to start preheating with the generator on the shaft rotating gear by means of which the shaft is rotated at approximately two revolutions per minute. Application of the preheating current is not permitted at standstill in order to avoid burning of the collector rings.

2. With the generator rotated by the turbine, not faster than 300 rpm, or with the generator on the rotating gear, the preheating current of 680 amperes (84 per cent of rated current) is applied to the rotor winding. Both generator cooling fans are started and provide practically normal air flow through the generator.

3. When the rotor winding approaches the specified preheat temperature, cooling water is admitted to control the temperature. The preheating current is held unchanged at a constant value. The preheat rotor-winding temperature is specified in relation to the prevailing cooling-water temperature as follows:

Cooling Water Temperature (Deg F)	Preheat Winding Temperature (Deg C)
Up to 45	65
46 to 60	73
61 and over	81

4. When the specified preheat temperature is reached and when the turbine is sufficiently warmed up, the generator is shut down to standstill and immediately restarted. This is done to permit complete relief of all stresses that may have accumulated in the winding. The preheating current still remains unchanged.

5. The turbine is now rapidly accelerated. As the speed increases above 1,000 rpm the operator reduces the preheating current just enough to prevent excessive generator voltage. When the generator reaches synchronizing speed the preheating current is removed, the shaft exciter is quickly switched to supply normal excitation, the generator is synchronized, and then loaded.

During the shutting-down period the cooling-water flow through the air coolers is reduced as the load is decreased to prevent excessive cooling, thus shortening the time required for subsequent preheating.

Calculations in Appendix II show that by using the above preheating procedure the stresses in the winding are 4,510 pounds per square inch in compression at maximum load and 3,790 pounds per square inch in tension at minimum load. Without preheating and starting at room temperature, the winding stresses would become 10,980 pounds per square inch in compression. It is expected that no

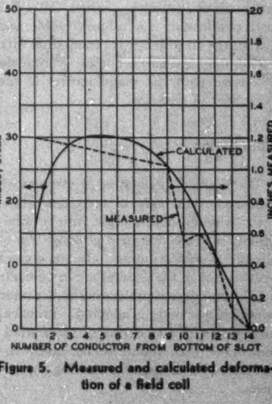

Figure 5. Measured and calculated deformation of a field coil

noticeable winding deformation will occur with the adopted preheating procedure.

The preceding example is an ideal one since facilities exist which permit the control of all of the diverse factors. Where such facilities do not exist, a great deal can still be gained by preheating in a less complete manner. So long as the rotor is brought to within its operating temperature range a material reduction in deformation can be expected.

Overloading of Generators

Frequently, particularly under the pressure of the present emergency, the problem arises whether a given generator can be overloaded with currents in excess of the rated field current and armature current. This problem becomes particularly pressing where a generator has a rated load temperature rise below that permissible by existing AIEE Standards. Very often this problem arises from the necessity to carry lower-power-factor loads and less often it arises from the availability of additional capacity in the turbine.

When temperature rise is less than the permissible at rated load it must not be assumed that the manufacturer had miscalculated in the design of the generator and in that way had really provided a generator larger than required. A safer assumption is that the manufacturer in designing the given generator was forced to compromise conflicting requirements with the result of having to design for a lower temperature rise in order to meet some other more pressing limitation. Among these limitations may well be that of field-winding deformation and if this be the major limitation, it may be possible to permit an appreciable increase in field current if preheating is adopted. Whether such overloading is at all permissible, and the extent to which it is permissible with preheating, is a subject to be thoroughly discussed with the manufacturer of the generator in question.

Conclusion

By means of preheating of rotor windings and rotor body of turbogenerators, rotor winding deformation can be minimized, with a consequent prolongation of the life of such windings. In some cases overloading of the generator can be made a safe procedure if preheating of the rotor windings and body is adopted. Practical procedures for preheating of rotor windings have been found to be easily coordinated with turbine-starting operations.

Appendix I

Data pertaining to the 160,000-kva 25-cycle 1,500-rpm turbogenerator.

Generator data:

Rating	160,000 kva
Power factor	1.0
Voltage	11.4 kv
Frequency	25 cycles
Maximum excitation voltage	250 volts
Rated field current	815 amperes

Generator-cooling-system data:

Type	Surface air coolers
Blowers	Separate motor and turbine driven
Quantity of cooling air	160,000 cubic feet per minute
Total heat loss, full load	2,245 kw
Total heat loss, no load	1,595 kw
Heat loss in rotor, full load	235 kw
Quantity of cooling water, maximum	1,000,000 pounds per hour
Maximum cooling water temperature	75 degrees Fahrenheit

Physical data:

Coefficient of linear expansion, copper—1.7×10^{-5} inch per inch per degree centigrade

Coefficient of linear expansion, steel—1.15×10^{-5} inch per inch per degree centigrade

Modulus of elasticity of copper—1.6×10^{7} pounds per square inch per inch

Generator operating data:

	Maximum Load	Minimum Load
Field current, amperes	875	460
Average rotor winding temperature, degrees centigrade	111.0	55.9
Average rotor body temperature, degrees centigrade	85.0	48.7
Airgap air average temperature, degrees centigrade	53.5	40.0
Air temperature into generator, degrees centigrade	40.0	30.4
Cooling water temperature, degrees Fahrenheit	75.0	75.0
Daily load cycle, hours	10*	12*

Appendix II

Example of calculation of the preheating procedure for a 160,000-kva 1.0-power-factor 25-cycle 1,500-rpm generator with data of Appendix I.

In the following all primed symbols refer to the minimum load condition and all double-primed symbols refer to the maximum load conditions and unprimed symbols refer to preheating conditions. All w subscripts refer to the rotor winding and all B subscripts refer to the rotor body.

From Appendix I:

$T_w'=55.9$ degrees centigrade
$t_w''=111.0$ degrees centigrade
$T_B'=48.7$ degrees centigrade
$T_B''=85.0$ degrees centigrade
$t'=12$ hours
$t''=10$ hours

Using equations 11 and 12 of Appendix III

$$T_w=\frac{t'T_w'+t''T_w''}{t'+t''}$$
$$=\frac{(12\times55.9)+(10\times111)}{12+10}$$
$$=81 \text{ degrees centigrade}$$

$$T_B=\frac{t'T_B'+t''T_B''}{t'+t''}$$
$$=\frac{(12\times48.7)+(10\times85)}{12+10}$$
$$=65.2 \text{ degrees centigrade}$$

The preheating current is then

$$I=I''\sqrt{\frac{T_w-T_B}{T_w''-T_B''}}=875\sqrt{\frac{81-65.2}{111-85}}$$
$$=681 \text{ amperes}$$

The resulting stresses in the copper are found as follows:

$$s_w''=\alpha_w(T_w''-T_w)=1.7\times10^{-5}\times(111.0-81)=51.0\times10^{-5}$$
$$s_B''=\alpha_B(T_B''-T_B)=1.15\times10^{-5}\times(85-65.2)=22.8\times10^{-5}$$

then

$$f''=M(s_w''-s_B'')=1.6\times10^{7}\times(51.0-22.8)\times10^{-5}=4{,}510 \text{ pounds per square inch}$$

This stress is produced in compression at the highest operating temperature. At the minimum load conditions:

$$s_w'=\alpha_w(T_w-T_w')=1.7\times10^{-5}\times(81-55.9)=42.7\times10^{-5}$$
$$s_B'=\alpha_B(T_B-T_B')=1.15\times10^{-5}\times(65.2-48.7)=19.0\times10^{-5}$$
$$f'=M(s_w'-s_B')=1.6\times10^{7}(42.7-19.0)\times10^{-5}=3{,}790 \text{ pounds per square inch}$$

This stress is in tension.

For comparison of the efficacy of preheating, the stress produced during starting from an ambient of 40 degrees centigrade to maximum operating temperature is calculated:

$$s_w=\alpha_w(T_w''-40)=1.7\times10^{-5}(111-40)=120.4\times10^{-5}$$
$$s_B=\alpha_B(T_B''-40)=1.15\times10^{-5}(85-40)=51.8\times10^{-5}$$
$$f''=M(s_w-s_B)=1.6\times10^{7}(120.-51.8)\times10^{-5}=10{,}980 \text{ pounds per square inch}$$

Nomenclature:

α_w = coefficient of expansion for the winding
α_B = coefficient of expansion for the rotor body
s = strain
f = stress
M = modulus of elasticity

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* The remaining two hours are used in raising and lowering of load.

Appendix III

Nomenclature (for additional symbols see Appendix II):

T = temperature in degrees centigrade
a, k = constants
s, s_0 = plastic strains
u = creep rate
ϵ = base of the natural logarithm

Within the range of temperature and the range of stress occurring with preheating the creep rate is approximated by the following relation

$$u=\frac{ds}{dt}=af\phi(T) \qquad (1)$$

where $\phi(T)$ is a function of the absolute temperature. For short-time creep rates very little is known about this function, although various attempts have been made[2] to approximate test results in long-time test. For this reason the function $\phi(T)$ is in the following analysis taken to be constant in the belief that this step will not greatly affect the result.

If an elementary length of conductor is stressed and then held so that its length remains invariant, the stress is relieved by creep according to the relation

$$-M\frac{ds}{dt}=\frac{df}{dt}=aMf \qquad (2)$$

Setting

$$k=aM$$

for convenience,

$$-\frac{df}{dt}=kf \qquad (3)$$

which has the solution

$$f=f_0\epsilon^{-kt} \qquad (4)$$

where f_0 is the stress at the time $t=0$
Since

$$f_0=Ms_0 \qquad (5)$$

and since

$$\begin{aligned} s_0'' &= \alpha_w(T_w''-T_w)-\alpha_B(T_B''-T_B)\\ s_0' &= \alpha_w(T_w-T_w')-\alpha_B(T_B-T_B') \end{aligned} \qquad (6)$$

The total strains accumulated during the times t'' and t' become

$$S_0''=[\alpha_w(T_w''-T_w)-\alpha_B(T_B''-T_B)]\times \int^{t''}\epsilon^{-kt}dt$$

$$S_0'=[\alpha_w(T_w-T_w')-\alpha_B(T_B-T_B')]\times \int^{t'}\epsilon^{-kt}dt \qquad (7)$$

Integrating equations 7 and substituting the approximation

$$\begin{aligned} 1-\epsilon^{-kt} &= kt\\ S_0'' &= [\alpha_w(T_w''-T_w)-\alpha_B(T_B''-T_B)]kt''\\ S_0' &= [\alpha_w(T_w-T_w')-\alpha_B(T_B-T_B')]kt' \end{aligned} \qquad (8)$$

When the load cycle is complex equations 8 are used to determine T_w and T_B by trial and error. In most cases the load cycle is sufficiently approximated by calculation for maximum and minimum loads only. In the latter case equations 8 allow further analysis. Set

$$S_0''=S_0'=[\alpha_w(T_w''-T_w)-\alpha_B\times (T_B''-T_B)]kt''=[\alpha_w(T_w-T_w')-\alpha_B(T_B-T_B')]kt' \qquad (9)$$

Substituting in equation 9

$$r=\frac{\alpha_B}{\alpha_w}$$

and solving for T_w and t_B:

$$T_w-rT_B=\frac{t'T_w'+t''T_w''}{t'+t''}-r\frac{t'T_B'+t''T_B''}{t'+t''} \qquad (10)$$

Since T_w and T_B in equation 10 are both unknown further, strict mathematical treatment is impossible. However, if

$$T_w=\frac{t'T_w'+t''T_w''}{t'+t''} \qquad (11)$$

and if

$$T_B=\frac{t'T_B'+t''T_B''}{t'+t''} \qquad (12)$$

are used, a satisfactory solution is obtained.

Appendix IV

When heating or cooling of a body, having thermal capacity, takes place in such a manner that the heat loss per unit time is directly proportional to temperature, the following relations apply.
Let

C = thermal capacitance in kilowatts per degree centigrade per hour
k = proportionality constant of heat dissipation in kilowatts per degree centigrade
H = heat loss in kilowatts
θ = temperature rise
T = time constant
t = time in hours

For heating

$$C\frac{d\theta}{dt}+k\theta=H \qquad (13)$$

For cooling

$$C\frac{d\theta}{dt}+k\theta=0 \qquad (14)$$

Equations 13 and 14 have the solutions

$$\theta=\theta_0\left(1-\epsilon^{-\frac{t}{T}}\right) \qquad (15)$$

or

$$\theta=\theta_0\epsilon^{-\frac{t}{T}} \text{ respectively} \qquad (16)$$

where

$$T=\frac{C}{k}$$

and where

θ_0 = ultimate temperature rise

For example, using values of Appendix I:
For the rotor windings

$$C_w=\frac{C_1W_1}{3{,}413}=\frac{0.167\times 20{,}000}{3{,}413}=0.974 \text{ kilowatt per degree centigrade per hour}$$

For the rotor body

$$C_B=\frac{C_2W_2}{3{,}413}=\frac{0.212\times 200{,}000}{3{,}413}=12.4 \text{ kilowatts per degree centigrade per hour}$$

where

C_1 = specific heat of copper in kilowatt-hours per pound per degree centigrade
C_2 = specific heat of steel in kilowatt-hours per pound per degree centigrade
W_1 = weight of windings in pounds
W_2 = weight of rotor body in pounds

For the rotor winding

$$k_w=\frac{H}{\theta_{w_0}}=\frac{235}{26}=9.04 \text{ kilowatts per degree centigrade}$$

For the rotor body

$$k_B=\frac{H}{\theta_{B_0}}=\frac{235}{31.5}=7.46 \text{ kilowatts per degree centigrade}$$

Where θ_{w0} and θ_{B0} are the ultimate temperature rises of the rotor winding over the rotor body and of the rotor body over airgap air, respectively.

$$T_w=\frac{C_w}{k_w}=\frac{0.974}{9.04}=0.1075 \text{ hour}$$

$$T_B=\frac{C_B}{k_B}=\frac{12.4}{7.46}=1.67 \text{ hours}$$

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Blast-Tube Cooling for Aircraft Generators

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Synopsis: Established principles of hydraulics are used to develop and explain fundamentals of blast-tube cooling. It is shown that the pressure-volume curves of a blast tube can be determined by flight tests, and that the pressure-volume curve of a generator can be determined in the laboratory. From the two pressure-volume curves, the air-flow through the generator in flight can be predicted, without *an actual flight test on the generator*. This procedure would save many flight tests, if pressure-volume curves of various blast tubes were available. It is shown that the pressure-volume curve of a blast tube can be determined by open-end and closed-end readings on the blast tube, and the complete curve can be expressed in terms of two constants readily determined from these readings. Once values of the constants are established, air-flow calculations become as simple as the application of Ohm's law. The theory also covers generators with an internal fan. A plea is made for accurate blast-tube data and for better specifications on blast cooling. A suggested form of blast-tube specification is given.

BLAST cooling has been a major factor in obtaining the phenomenal outputs of modern aircraft generators from a small weight of material. This subject, in spite of its importance, seems to have been neglected in technical literature and, as a result, the fundamental principles do not seem to be at all well understood. Because of this lack, the subject has often appeared to be more involved than it actually is. It is generally understood that the amount of air flowing through a given generator depends upon the pressure differential available across the generator, and it is also well understood that the amount of pressure available from the blast tube depends upon the amount of air drawn by the generator. Because of this mutual interdependence, flight tests are today generally taken with a specific generator and blast tube, and it seems to be generally felt that it is necessary to test each different combination of generator and blast tube. Just as Ohm's law enormously simplified the calculation of electric circuits, so can the calculation of blast cooling be simplified by the use of similar concepts, which will be defined, explained, and used in this paper. This line of attack clarifies the fundamental principles involved, makes it necessary to run only one series of tests on any given blast tube, and one series of tests on any one given generator. It is not necessary to test each and every combination of generator and blast tube. This line of attack also makes it possible to write specifications which simultaneously accomplish a number of purposes:

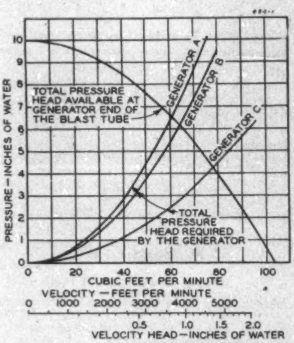

Figure 1. Pressure-volume curves of a blast tube and three representative aircraft generators

(*a*). The airframe builder has a definite specification which can be used not only to design the blast-tube installation, but also to test it. He need not be concerned with different generator designs.

(*b*). The generator builder has a definite specification that permits him enough latitude to design a generator for maximum effective use of the available air.

(*c*). The specification clearly indicates how much pressure should be applied to the generator in laboratory tests to *simulate flight* conditions. (This point should be of especial interest to a procuring agency.)

The scope of this paper is limited to a study of air flow; it is not directly concerned with heat transfer and rating.

Pressure-Volume Curve of a Blast Tube

Pressure and volume flow in a blast tube can both be measured by means of the familiar pitot tube and either a manometer or an air-speed indicator. Three kinds of pressure can be read: static head, velocity head, and total head, which is the sum of the other two. In this paper, all pressures, except atmospheric, are measured in inches of water. Total pressure is a measure of the total energy in the air stream, and velocity head, for a fixed density, is a measure of the volume of air flowing.

For purposes of illustration, assume a specific blast-tube installation in a plane which is flying at a constant indicated air speed, and further assume that the position of oil-cooler flaps, or any other air dampers that might affect the flow of generator cooling air, remains fixed. Let the total pressure at the generator end be measured with the end capped so that no air can flow. Now, let the end be partially uncapped so that some air can flow, and let the volume and total pressure at the end of the blast tube be measured. This reading can be repeated for a number of different volumes until the tube is wide open, and the total pressure is then only the velocity head at exit. If now, the total head is plotted against the number of cubic feet of air drawn per minute, a curve will result, probably of the form shown in Figure 1, which is a parabola. This curve has been established without using a generator and may be called the "pressure-volume" curve of the blast tube. While it has not been established experimentally that the curve is a true parabola, the science of hydraulics indicates that it probably approximates one.

Pressure-Volume Curve of a Generator

Experiment and theory alike demonstrate that the pressure-volume curve of a generator is substantially a simple parabola. Three such curves, determined experimentally, are shown in Figure 1. The curve for each generator shows, for example, the pressure required to force any given volume of air through it. The three points where the three curves intersect the pressure-volume curve of the blast tube show the respective pressures and volumes that will be obtained by each generator. Thus the pressure and volume that each generator would receive from this blast tube can be determined without actually testing each generator with this blast tube.

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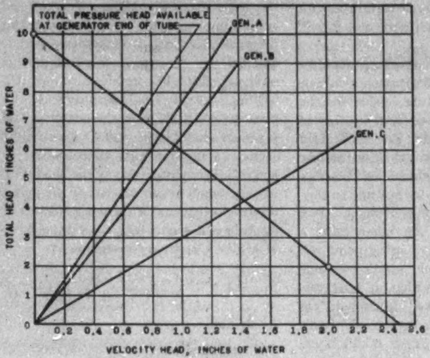


Figure 2. Pressure-volume curves of Figure 1 replotted as a function of velocity head

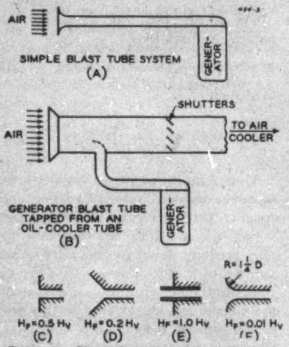


Figure 3. Blast-tube arrangements. Effect of shape of the entrance

The three generators for which the curves are shown are actual aircraft generators of different design which are in production use in aircraft today. None of them has an internal fan. The pressure-volume curve of the blast tube, necessary for purposes of illustration, was estimated since no experimental curve was available. However, the pressures shown for the three generators approximate the pressures which have been observed in flight. These curves show how unreliable it is to impress the same pressures upon different generators for comparative tests. For example, suppose six-inch total head is impressed upon all three generators for test. The volumes of air that would be received with six-inch head applied are compared with the volumes received under flight conditions in the following table:

Table I. Tested Values of Air-Flow Resistance, R_g, for Three Different Designs of Aircraft Generators

(Based on a Two-Inch Blast Tube)

Conditions of Test	Generator A	Generator B	Generator C
Generator cold			
At standstill	6.51	5.90	2.80
At 2,500 rpm	6.82	5.71	2.87
At 3,500 rpm	6.94	6.04	2.90
At 4,000 rpm	7.15	6.19	2.91
After carrying 75 per cent load for one hour			
At 3,500 rpm	7.60	6.45	2.98

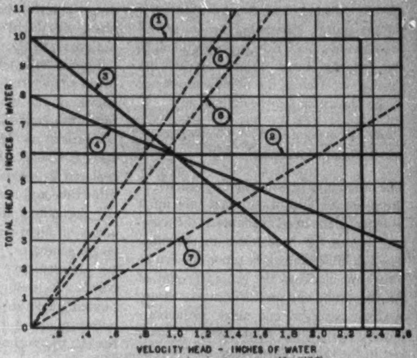


Figure 4 (right). A comparison of blast-tube specifications

	Test	Flight
Generator *A*	58	61
Generator *B*	63	64
Generator *C*	93	78

Thus generator *C* would show 19 per cent higher temperature rise in flight than it would on laboratory test at six inches, although the tests on the other two generators would be approximately correct.

Alternative Method for Plotting Pressure-Volume Curves

In Figure 1, total pressure has been plotted as a function of volume. In Figure 2, the curves of Figure 1 are replotted with total pressure as a function of velocity head rather than volume. The pressure-volume curves become straight lines, which fact is an important advantage of this method of plotting. But much more important is the fact that, for all practical purposes, the curves of Figure 2 *are independent of air density, hence independent of altitude,* whereas the curves of Figure 1 are valid for only one air density. This statement is merely another way of stating the familiar principle of hydraulics that the lost head is proportional to the velocity head. Figure 1 could be valid for all altitudes, however, if the abscissae were understood to be "*indicated* cubic feet per minute," but the form of Figure 2 seems to be more convenient. Another advantage of using and plotting velocity head is that, when a pitot tube is used, *velocity head* is the quantity that is actually measured. Even if an air-speed indicator is used instead of a manometer, it is still velocity head which operates the instrument, though the scale is calibrated in miles per hour.

Calculation of Generator Pressures During Flight

In Figure 2, the pressure head across generator *A* and the velocity head (indicative of volume) of the air supplied was determined by finding the intersection of the pressure-volume curve of the blast tube with the pressure-volume curve of the generator, since the amount of head lost in the generator obviously has to be equal to that which is available from the blast tube. By using a concept of air-flow resistance, analogous to electrical resistance, this point of intersection can be calculated easily without plotting the curves of Figure 2. For any generator, the ratio

$$\frac{\text{head loss in generator}}{\text{velocity head at entrance}} = \frac{h_g}{h_v} = R_g \qquad (1)$$

is a constant, substantially independent of the pressure applied. This ratio R_g, may be called the "air-flow resistance" of the generator. Similarly, the amount of head lost in a blast tube is proportional to the velocity head of the air flowing through it. Hence, the air-flow resistance of the blast tube may be defined as

$$\frac{\text{head lost in blast tube}}{\text{velocity head in blast tube}} = R_t \qquad (2)$$

The amount of pressure head available at the generator end of the blast tube must be equal to the head available at the scoop end, minus the head losses in the tube. The available head at the scoop end can be found by blocking the generator end of the blast tube and measuring the pressure h_c at the generator end, since, with no air flowing in the tube, there will be no head lost in the tube. Then, since the losses in the tube plus the losses in the generator must equal the available head, it follows from equations 1 and 2 that

$$h_c = h_v R_t + h_v R_g \qquad (3)$$

or

$$h_v = \frac{h_c}{R_t + R_g} \qquad (4)$$

The pressure across the generator is

$$h_g = h_v R_g = \frac{R_g}{R_t + R_g} \times h_c \qquad (5)$$

By way of illustration

For the blast tube of Figure 2

$h_c = 10.0$
$R_t = 4.0$

For generator B, $R_g = 6.45$
from equation 4

$$h_v = \frac{10}{4 + 6.45} = 0.958 \text{ inch}$$

and from equation 5

$$h_g = h_v R_g = 0.958 \times 6.45 = 6.16 \text{ inches}$$

which agrees with point of intersection shown in Figure 2.

Each blast-tube installation will have a different pressure-volume curve for each different indicated air speed of the plane. Likewise, if the generator blast tube is tapped off from a larger blast tube leading to an oil cooler, or intercooler, there will be a different pressure-volume curve for each position of the shutters in the main blast tube. Thus, the true performance of any given blast-tube installation can be represented by a family of pressure-volume curves, one for each significant flight condition. Each of these pressure-volume curves can be expressed, approximately at least, in terms of two significant constants, h_c and R_t. It is the author's opinion that, in any given installation, it is h_c which will show large variations with changes in flight conditions, and R_t will not vary greatly.

Flight studies of blast cooling should be made to determine the pressure-volume curves of the blast tube, either for the minimum condition, or for a number of conditions, as desired. For such flight tests, it is suggested that the generator be replaced by a device which incorporates a pitot tube and means for opening or closing the end of the tube in flight. If a series of flight tests are taken using a single generator, only a single point will be obtained on each pressure-volume curve. It is obvious that at least two points, preferably well separated, are necessary to establish a curve. For this reason, the author suggests taking open-end and closed-end readings, since these two readings establish the complete pressure-volume curve, or at least a fair approximation of it. More points could be taken, if greater accuracy is desired. However, regardless of the number of points taken, if the curve of pressure versus velocity head can be represented by a straight line, this line can be represented by two constants h_c and R_t, representing developed pressure and tube resistance, respectively. When h_c and R_t are known, and when the air-flow resistance R_g of a generator is known, the total pressure across it, and the velocity head can be quickly and easily computed from equations 4 and 5. From the velocity head, the volume and weight can be determined as indicated in the following paragraphs.

Determination of Air Velocity, Volume, and Weight From h_v

For National Advisory Committee for Aeronautics sea-level conditions of 15 degrees centigrade and 29.92 inches mercury, it can be shown that

$$v_m = 3{,}967\sqrt{h_v} \qquad (6)$$

(This equation is based upon an air density of 0.07651 pound per cubic foot, water density = 62.37 pounds per cubic foot, and acceleration of gravity = g = 32.18. It further assumes that the air is incompressible.) At any altitude, where the density of the air is ρ pounds per cubic foot

$$v_m = \frac{1{,}097}{\sqrt{\rho}}\sqrt{h_v} \qquad (7)$$

Or, in terms of temperature T (degrees centigrade) and atmospheric pressure b in inches of mercury

$$v_m = \left(1{,}279\sqrt{\frac{273+T}{b}}\right)\sqrt{h_v} \qquad (8)$$

Or, if an air-speed indicator is used, the speed may be read in miles per hour, the pressure altitude and temperature noted, and the true air speed computed with a navigational flight computer.

For all velocity-head measurements, the pitot tube should be in the center of the pipe and should be preceded by a straight section of pipe at least 10 diameters in length. Average velocity in the blast tube may be taken as 80 per cent of the maximum velocity; and, if the sectional area of the tube is A square feet, the volume flow, at sea level, is, from equation 6

$$V = 0.80 \times v_m \times A = 3{,}967 \times 0.80 \times A\sqrt{h_v} = 3{,}174A\sqrt{h_v} \qquad (9)$$

At altitude

$$V = 0.80 \times v_m \times A = \left(1{,}023A\sqrt{\frac{273+T}{b}}\right)\sqrt{h_v} \qquad (10)$$

The weight flow of air, in pounds per minute, is at sea-level

$$W = 242.8A\sqrt{h_v} \text{ pounds per minute} \qquad (11)$$

at altitude

$$W = \left(753.4A\sqrt{\frac{b}{273+T}}\right)\sqrt{h_v} \qquad (12)$$

Thus, from the measured velocity head h_v, can be computed velocity, volume, and weight. If the generator builder is given values of h_c and R_t for the blast tube, he only needs to determine the air-flow resistance of a new generator in order to compute the velocity head, and from it the volume and weight flow for this new design without waiting for a flight test. Obviously, the same process could be applied to several alternative designs to determine which one would be best in a plane from the standpoint of effective cooling.

The Concept of Air-Flow Resistance

Air-flow resistance has been defined in this paper in a manner analogous to resistance in an electric circuit, so that it can generally be used in a manner analogous to the use of ohms in calculating d-c circuits. For example, since the generator and blast tube are in series, their "air-flow resistances" can be added directly. But a word of caution is necessary. The air-flow resistances of all the elements in any given air circuit must all be computed on the basis of the same diameter of tube at which the velocity head is measured. For example, the air-flow resistance of generator A is 7.60 based on a two-inch blast tube; if the diameter of the entrance were changed to three inches without changing the generator otherwise, the resistance would then be $7.60\times(3/2)^2=17.1$.

Air-flow resistance has been used in a number of unpublished reports, defined in various ways. To avoid confusion, it is suggested that a name be applied to the unit of air-flow resistance as defined in this paper. Since this unit is so defined that a resistance of n units means a lost head of n velocity heads, the name "velhead" is suggested as appropriate. For example, generator A has an air-flow resistance of 7.60 velheads based on a two-inch tube. If no tube diameter is specified, it will be generally understood that the resistance value is based on the actual diameter of the air entrance of the generator.

Generators With Internal Fan

An internal fan will have the general effect of increasing the air flow. Let the pressure of the internal fan be denoted by h_{gf}. This pressure will be proportional to the square of the revolutions per minute and to the first power of the air density; it can be represented by the equation

$$h_{gf}=F_p\times\left(\frac{N}{1{,}000}\right)^2\times\frac{\rho}{\rho_0} \qquad (13)$$

or

$$h_{gf}=F_p\times\left(\frac{N}{1{,}000}\right)^2\times\frac{0.9626b}{273+T} \qquad (14)$$

where F_p is an empirical constant. Physically, it is the pressure developed by the fan at sea level at 1,000 revolutions per minute. This constant F_p might be measured by blocking either the inlet or the outlet and measuring the pressure at the blocked end of the generator; readings should be taken at different speeds.

When supplied by a blast tube, the velocity head of air flowing may be computed from equation 4 by adding the fan pressure to the numerator or

$$h_v=\frac{h_c+h_{gf}}{R_t+R_g} \qquad (15)$$

The volume can then be computed by using equation 10 and the weight of cooling air by using equation 12. The total pressure h_g across the generator is

$$h_g=R_gh_v-h_{gf}=\frac{R_gh_c-R_th_{gf}}{R_g+R_t} \qquad (16)$$

Using these equations, it can be shown that if to generator B a fan were added capable of delivering 1.5 inches pressure at sea level, the volume of air through the generator would be increased seven per cent, but the pressure across the generator would be reduced from 6.16 inches to 5.60 inches, a decrease of nine per cent. (Without numerical values of the two constants of the blast tube, h_c and R_t, these calculations could not have been made.) It is obvious that the addition of the fan should increase the volume of air and decrease the pressure at the inlet of the generator; but to determine quantitatively how great this effect is, it is necessary to know the two fundamental constants of the blast tube, h_c and R_t.

Suggested Tests for Blast Tubes

For a given installation, and for a given set of conditions, that is, for a given indicated air speed and position of the shutters in any air ducts associated with the generator blast tube, two readings are required: an open-end reading, and a closed-end reading. A pitot tube is installed in a straight section of tubing $1^{15}/_{16}$ inches in diameter approximately 20 inches long; the impact tube should be in the center of the pipe. The total-head connection of the pitot tube is to be connected to the pressure side of the manometer, or air-speed indicator; the static-head connection should preferably be capped, and the exhaust side of the manometer should be connected to the chamber into which the generator would discharge its exhaust air. Provision must be made so that the end of the blast tube can be either sealed tight or opened wide during flight, at will. For each significant set of flight conditions, two readings of the blast tube are to be taken, as follows:

(*a*). *Closed-end reading*, h_c = total pressure head above generator air discharge chamber, in inches of water.

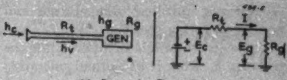
(*b*). *Open-end reading*, h_o = total pressure head above generator air discharge chamber, in inches of water. Record actual inside diameter of the pipe at the location of the pitot tube.

These tests are similar in principle to the classical open-circuit and short-circuit tests so frequently taken on various kinds of electrical machinery. The two blast-tube constants can be readily computed from these two readings as follows:

$$h_c=h_c \qquad (17)$$

$$R_t=\frac{h_c-h_o}{h_o} \qquad (18)$$

h_o is a velocity head since the static head when the tube is discharging freely, is zero. The altitude at which the plane is flying for these readings is not particularly important, since the density of the air should not affect the test results appreciably. If the velocity of air in the blast tube is desired for some reason or other, it may be determined from equation 8, the volume from equation 10, or the weight from equation 12.



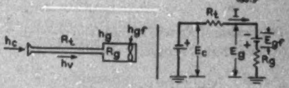
No Fan in the Generator

Closed-end head = h_c
Blast-tube resistance = R_t
Velocity head of flow = h_v
Generator resistance = R_g

E_c = open-circuit volts
R_t = line resistance
I = current
R_g = load resistance

$$h_v=\frac{h_c}{R_t+R_g} \qquad I=\frac{E_c}{R_t+R_g}$$

$$h_g=h_vR_g=\frac{R_gh_c}{R_t+R_g} \qquad E_g=IR_g=\frac{R_gE_c}{R_t+R_g}$$



With Internal Fan in the Generator

$$h_v=\frac{h_c+h_{gf}}{R_t+R_g} \qquad I=\frac{E_c+E_{gf}}{R_t+R_g}$$

$$h_g=h_vR_g-h_{gf} \qquad E_g=IR_g-E_{gf}$$

$$=\frac{R_gh_c-R_th_{gf}}{R_t+R_g} \qquad =\frac{R_gE_c-R_tE_{gf}}{R_g+R_t}$$

Note: If h_g is —, generator fan helps overcome R_t as well as R_g, and more air would flow through generator if blast tube were removed from generator.

Note: If E_g is —, E_{gf} helps overcome R_t as well as R_g, and more current would flow if terminals E_g were short-circuited.

Figure 5. Electric circuit analogue

Tests to Determine Air-Flow Resistance of Generators

Generator air-flow resistance can be measured in the laboratory by applying a total head, h_g, to the generator air inlet, and measuring the velocity head, h_{vg}, in the blast tube. The air flow resistance of the generator is:

$$R_g=h_g/h_{vg} \qquad (19)$$

Values of R_g were determined experimentally by G. G. Setterlund, who obtained the results shown in Table I.

These results indicate that there is some variation in air-flow resistance with temperature and with speed and, although these changes are comparatively small, it is recommended that the air-flow resistance be measured at conditions ap-

Table II. Air Pressures on Generators During Flight

(Reference, Figure 4)

	Generator A	Generator B	Generator C
According to curve 1	10	10	6.9
According to curve 2	6	6	6
According to curve 3	6.55	6.17	4.28
According to curve 4	6.33	6.10	4.79

proximating full-load temperature and normal operating speed.

If the generator has an internal fan, the pressure h_{gf} from it at normal operating speed should first be measured by blocking the inlet or exit. Then, with the machine operating at the same speed and at approximately full-load temperature, impress a head h_g and measure the velocity head h_{vg} of the inlet air. The air-flow resistance will be:

$$R_g = \frac{h_g + h_{gf}}{h_{vg}} \tag{20}$$

This value of Rg will include the resistance of the internal fan, as it should.

Factors Affecting h_e

If the air scoop were out on a wing where it was unaffected by the slip stream, as pitot tubes for air-speed indicators are, and if the air from the blast tube were discharged to atmospheric pressure, h_e would be simply the airplane pitot pressure, which is the same for all planes. It would be approximately

$$h_e = \left(\frac{\text{indicated miles per hour}}{45.1}\right)^2 \tag{21}$$

(For high air speeds, h_e would be slightly more than indicated by the above.) If the scoop is behind the propeller, h_e could be considerably above airplane pitot pressure. However, if the back pressure in the air discharge chamber is substantially above airplane static pressure, as tests indicate it often is, the net pressure h_e may be either greater or less than airplane pitot pressure, that is, greater or less than would be indicated by equation 21. An ideal arrangement would be a short tube with a minimum number of bends, with a curved-funnel scoop directly in the propeller blast, as shown in Figure 3A.

Quite different is the arrangement shown in Figure 3B where the generator air supply line is merely tapped into a larger blast tube supplying the oil cooler, intercooler, or both. In this case, h_e will depend not only upon the speed of the plane, but also upon the position of the shutters; h_e will be greater when they are closed than when they are open. The quantity h_e can be increased some by use of a scoop at the entrance to the generator tube to take advantage of the velocity head of the air in the main duct. For this arrangement, the worst condition is with shutters wide open and plane flying at minimum speed.

If a flexible air tube is used, there may be considerable air leakage, which will reduce appreciably the observed values of h_e and h_v.

Factors Affecting R_t

The air-flow resistance of the blast tube, R_t takes into account the head losses in the blast tube including entrance losses. Numerically, it is equal to the number of velocity heads lost in the tube. The losses consist principally of: entrance losses, fluid friction at the sides of the tubes, losses in elbows or bends in the tube, and losses from changes in cross-sectional area of the tube, if any. The loss at the entrance is affected by the shape of the entrance. Figures 3C to 3F show different shapes of entrances and the probable head loss due to each. (These are the head losses usually obtained when a constant static head is maintained at the entrance; whether or not the same losses are obtained when the tube itself is moved against the air is problematical, but it seems reasonable to suppose that these figures do afford a comparison of the relative effects of different shapes of entrance.)

In general, in any fixed plane installation, the tube resistance R_t probably does not change greatly with changes in plane speed or changes in shutter position. However, there may be an exception in the case of an installation such as Figure 3B if the amount of air drawn by the generator is an appreciable part of the total air, because the air drawn by the generator will increase the drop between the entrance of the main scoop and the entrance to the generator tube. Thus, with shutters closed, both h_e and R_t may be higher than when the shutters are open.

Blast-Tube Specifications

Pressure-volume curves corresponding to four different blast-tube specifications are given in Figure 4, together with pressure-volume curves of three representative aircraft generators. Curve 1 illustrates specification 95-32275, amendment 2, dated March 3, 1942, which specifies ten inches total pressure, but not over 100 cubic feet per minute; the vertical line is drawn at a velocity head corresponding to 100 cubic feet per minute. Curve 2 illustrates specification ANG-1-13 which specifies six inches of water, with no limit on volume. No blast tube could have a pressure-volume curve corresponding to either curve. Curve 3 is the pressure-volume curve repeated from Figures 1 and 2 of this paper. Curve 4 shows a different idea as to what the true pressure-volume curve may be. According to these specifications, the pressures received by the three generators in flight will be indicated by the points of intersection (Table II). Then, according to these specifications, the pressures impressed on the generator for chamber tests should be according to the table.

Taking curve 3 as a reasonable representation, it is suggested that the specification for a two-inch diameter blast tube might be worded somewhat as follows:

> **The blast tube shall be designed to deliver, for any condition of flight, a total pressure head at the end of the blast tube of at least ten inches of water when the end is closed and at least two inches of water when the end is not restricted. This pressure head shall be the total head at the end of the tube above the ambient air pressure in the generator compartment. When the open-end reading is being taken, the blast tube shall be discharging its air into the generator compartment and the pitot tube shall be in the center of a two-inch-diameter straight pipe preceded by a straight length of ten or more diameters.**

Such a specification would be more informative and helpful to a generator designer, than present specifications, because he could then determine in advance what air pressures and volumes his generator would receive in flight. Moreover such a specification would be of advantage to a procuring agency when any new generator was received because it could run a simple air-flow test on the generator in the laboratory, and by very simple calculations determine the pressure and volume the generator would receive in flight, *without actually making a flight test.* This same pressure could be applied in laboratory chamber tests. However, it is to be hoped that pressure-volume curves for representative installations will be obtained by actual flight tests so that specifications can be based on actual conditions.

It should be emphasized here, that there is no magic figure for the air requirement for any generator; in general the more air it gets, the better will be the performance, which may show up in the form of more load capacity, particularly at a high altitude, lower temperature rise, greater reliability, or longer life.

Conclusions and Remarks

Fundamental theories of blast-tube cooling have been developed and their use explained. The problem of blast cooling is broken down into two parts: a study of the blast-tube installation itself, and an independent study of the generator itself. It is shown that the performance of the blast tube can be expressed in terms of two factors: internal pressure h_e (that is, pressure available with no flow) and air-flow resistance R_t, a quantity analogous to ohms in a d-c circuit. Values of these two constants, which vary somewhat, depending upon the installation, upon plane speed and other factors, are all that is required to predict the air flow through a generator which has been tested independently of the blast tube. Tests on the generator alone can be made to yield, in the case of a generator with no internal fan, a single constant, the air-flow resistance R_g. If the generator has an internal fan, more tests are required on the generator to determine the characteristics of this fan, but no more tests are required on the blast-tube installation. In general, the method of attack is: first, the air-flow resistances of the generator and blast tube are obtained; second, the velocity head of air flow is computed from the closed-end pressure and resistances; third, the velocity, volume, and weight are then computed from the velocity head.

A more rational and illuminating method of specifying the minimum characteristics of a blast tube is proposed. The new method of specification would provide the airframe builder with a clear-cut specification which could be used for design and checking of the blast tube, it would provide the generator builder with a rational basis for designing his cooling system in the generator for maximum effectiveness, and, further, would provide a simple method for determining what pressure to apply to any given generator for sea-level tests, either at atmospheric pressure or in an altitude chamber.

The equations developed for blast-tube cooling are compared with similar equations for electric circuits in Figure 5.

Symbols

A = cross-sectional area of blast tube at pitot-tube location, square feet.

b = atmospheric pressure of air, absolute, in inches of mercury.

F_p = pressure coefficient of the internal fan of a generator. See equation 13.

H_f = lost head (Figure 3).

h_g = total head across the generator, inches of water.

h_{gf} = pressure developed by an internal fan, inches of water. See equation 13.

h_c = total pressure head at generator end of blast tube above generator ambient, end of tube closed.

h_o = total pressure head at generator end of blast tube above generator ambient, end of tube wide open and unobstructed.

h_v = velocity head, in inches of water.

h_{vg} = velocity head in blast tube when supplying air to a generator.

N = revolutions per minute of the generator.

T = temperature of air entering blast tube, in degrees centigrade.

R_g = air-flow resistance of a generator. Defined in equations 1, 19, and 20.

R_t = air-flow resistance of a blast tube. Defined in equation 2.

v_m = maximum velocity of air (at center of blast tube) in feet per minute.

V = volume of air flowing, in cubic feet per minute.

W = weight of air flowing, in pounds per minute.

ρ = density of air, in pounds per cubic foot.

ρ_0 = density of air at NACA sea-level condition (15 degrees centigrade, 29.92 inches of mercury) = 0.07651 pound per cubic foot.

Aids to Quality Control in the Manufacture of Aircraft Generators

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NONMEMBER AIEE

AIRCRAFT generators are designed for high output, low weight, small size, and wide speed range. These limitations require close designs which do not permit normal allowances for material and manufacturing variations. Small variations in the material and manufacturing processes result in wider variations in output characteristics than in conventional machines.

It is the purpose of this paper to discuss certain of these variations; their effect on the output characteristics of the machine, and to illustrate means of checking and controlling these factors.

Factors Affecting Output Characteristics

1. Shunt-field ampere turns available, compared to those required by the magnetic circuit, determine the base or underspeed characteristic.
2. Type of insulation, heat transfer coefficients, shape and size of cooling ducts, and the amount of cooling air taken through the generator determine the losses which can be dissipated; and in turn, the output rating.
3. Commutating ability, generator stability, temperature rise, and mechanical construction determine the top operating speeds.

Variations Affecting Required Shunt-Field Ampere Turns

For a required total flux, shunt field ampere turns are affected by the following:

1. Variations in air-gap length.
2. Variations in iron portion of the magnetic circuit.
   - (*a*). Magnet frame—permeability.
   - (*b*). Main poles—stacking factor and permeability.
   - (*c*). Armature punchings—stacking factor and permeability.
3. Variations in compensation of armature reaction.

Variations Affecting Temperature Rise

The temperature rise of an aircraft generator depends mainly upon:

1. The total losses.
2. The amount of cooling air.
3. The effectiveness of this cooling air; that is, where it goes and the area of winding surface it wipes.

Variations in Final Machines—Testing Technique

The assembled generator combines all the variations of its individual parts and then is subject to the variations of testing technique, which, when broken down, consist of: brush fit, brush setting, instrumentation, and the human element.

Variations in Air Gap Length

In the General Electric type *P*-1 aircraft generator rated 30 volts, 200 amperes, 2,200/4,500 rpm, forced-cooled, six-inches-water air pressure, approximately 70 per cent of the shunt field ampere turns are consumed in the nominal air gap. This part of the magnetic circuit, therefore, warrants all possible control of its variations.

Drawing tolerances, consistent with manufacturing capabilities and engineering requirements, permit a normal variation in the air gap. In addition to

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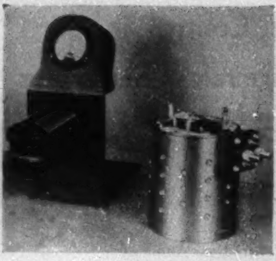


Figure 1. Equipment for determination of gaps between magnet frame and main poles

Figure 2. Test equipment for determination of magnet-frame permeability

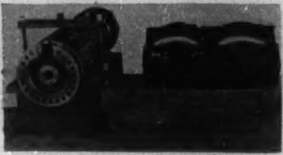


Figure 3. Equipment for spot-checking main-pole stacking factor and permeability

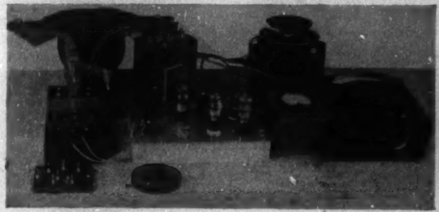


Figure 4. Armature-punching - permeability meter

the expected variations are the unexpected and undesired variations due to faulty manufacture and assembly of parts. It is these undesired variations which are eliminated by means of a special device termed a "gap checker." See Figure 1. In reality this device searches for and locates gaps between the main poles and the magnet frame.

The "gap checker" works on the bridge-circuit principle, using either flashing direct current or low-frequency alternating current as a source of power for the excitation coil. Intimate contact with the poles being checked is obtained by making the radius of the leg faces equal to that of the pole bore, and then supporting the entire weight of the field assembly from the contact faces.

If the total flux divides equally between the two parallel paths, the voltage induced in each of the bridge coils will be the same, and since they are connected opposing, no voltage will be read on the instrument. This indicates that both poles being checked are identical with regard to contact with the frame.

The air gap on the side opposite the poles being checked is large so that variations on this side of the field assembly will have little or no effect on the flux division in the legs of the device.

Experience in using this "gap checker" shows that there is usually a small voltage showing on the instrument if the poles have a normal fit against the frame. The center-zero instrument is therefore blocked off in red except for a small portion around the zero. If the needle goes into the red portion, one of the poles being checked has a gap in back of it. To determine which of the poles has the gap, the frame is rotated one pole pitch and the test repeated. If the high reading still exists, the pole of the original pair still being tested is the one at fault. If the high reading no longer exists when the frame is rotated one pole pitch, then the faulty pole is that one of the original pair which is not in the second pair.

Strictly, this device is a comparator, comparing the magnetic circuit of adjacent main poles. The use of such a device is based on manufacturing experience which shows that in those field assemblies faulted for this cause, there is usually only one faulty pole and seldom if ever more than two.

Variations in the Iron Portion of the Magnetic Circuit

MAGNET FRAME

During these wartime conditions, it has sometimes been found necessary to accept inferior and variable magnet-frame materials in order to keep the production lines going. Laboratory tests of several samples have shown the frame material to be extremely variable in its magnetic properties, some of it good, some of it definitely bad, and some of marginal properties. A laboratory test of each frame is obviously a production impossibility.

The "frame tester" shown in Figure 2 was therefore designed for making quick production line checks of the magnetic properties of the frame material.

The "frame tester" consists of a standard *P*-1 generator armature supported in a structure which includes one fixed main pole and one movable main pole 180 degrees apart. Field coils separately excited supply the required ampere turns. The armature is directly connected to and driven at constant speed by a one-quarter-horsepower induction motor. The voltage generated in the armature is picked up by a pair of brushes and read with a standard d-c voltmeter.

To check a frame, it is necessary to take only one reading. The frame to be tested is slid into position. Then, with the armature running, the field circuit is closed and a specified field current set. The armature voltage is then read. This reading is indicative of the magnet-frame permeability.

The value of this "frame tester" is that poor material is rejected at the earliest possible stage in the manufacturing cycle; that is, immediately after the boring and turning operation.

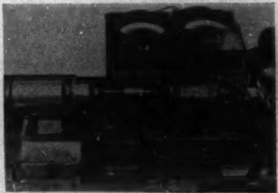


Figure 5. Equipment for preliminary test of armatures

Figure 6. Current-balance test equipment

MAIN POLES

Rigid adherence to specifications by the vendors of magnetic sheet steel, careful laboratory checks of all shipments received, and accurate control of annealing processes have resulted in little or no trouble magnetically in main poles.

However, as a guard against any trouble which might arise due to substandard poles, and thus interfere with production, the equipment shown in Figure 3 was made for making spot checks on various lots of poles.

The "pole tester" is similar in principle to the "frame tester" in that a voltage generated in a standard armature at a definite value of field excitation is the criterion for a passable pole. In this device the pole to be tested is slid through the opening in the mounting flange and screwed into position opposite a fixed pole around which the exciting coil is mounted. Poles to be so tested are ground to a definite radial length so that air-gap variations will not enter into the result.

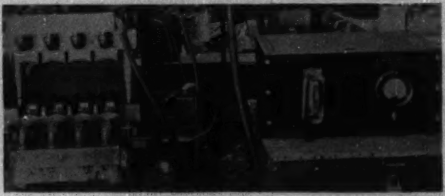


Figure 7. Shunt-field-coil mold and short-circuit tester

ARMATURES AND ARMATURE PUNCHINGS

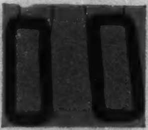
In addition to standard laboratory tests on magnetic sheet steel for armature punchings, it has been found desirable to make spot checks of armature punchings after the annealing process. The annealing process is of course an important operation and if done improperly, the punchings will not equal the required magnetic values.

An "armature-punching tester" is shown in Figure 4. A single armature punching makes up the magnetic circuit. Low-voltage 60-cycle alternating current obtained by means of a Variac transformer and a current transformer is applied to the one-turn winding through and around the core section of the armature punching. For a specified exciting current, the voltage induced in a five-turn secondary, which is proportional to the flux carrying properties of the punching, is read. Comparison of this reading to a minimum standard for the particular thickness of the punching indicates whether the punching is up to specified requirements.

Figure 5 shows equipment known as an "armature tester." This equipment consists of a pair of short poles mounted on a bed plate together with a pair of brush holders and brushes, a fixed ball-bearing holder on the drive end, and an adjustable ball-bearing holder on the opposite end which permits the device to be used for testing six-pole armatures of various heights of iron.

To test an armature, lapped ball bearings are placed on the armature shaft, the armature put into position, and the drive shaft brought into engagement with the inner splines of the armature shaft. The motor is then started up, a definite exciting current applied to the field coils, and the voltage generated by the armature read. If the voltage reading is below a specified value, the armature is rejected.

Figure 8. Shunt-field-coil turn counter



(A)

Figures 9A and 9B. Shunt and commutating coils, before and after molding

By means of this device, the following faults are determined:

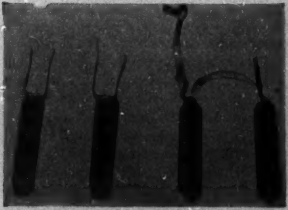
1. Poor magnetic properties of the stacked core.
2. Wrong connections of the armature winding.
3. Short circuits in the armature winding.

Variations in Compensation of Armature Reaction and Interpole Strength

The armature reaction per pole at full rated current of the General Electric type *P*-1 aircraft generator is 960 ampere turns as compared to a maximum of 480 ampere turns in the shunt field. Pole face or compensating windings are therefore used to neutralize partially the effect of the armature reaction. Interpoles and interpole windings are also used to obtain sparkless or "black" commutation throughout the speed and load range. These windings are connected in three parallel circuits, each circuit consisting of two commutating and two compensating coils in series.

To obtain the full benefit of these compensating- and commutating-pole windings, it is necessary that the three parallel circuits balance and that the load current divide equally between them.

Due to difficulty in accurately measuring these low-resistance circuits, a check of actual current balance is taken by means of the special setup shown in Figure 6. A small arc welder supplies approximately 75 amperes and the division of this current between the three field circuits is simultaneously read on the three ammeters. Limits of unbalance have been set beyond which fields are rejected. This test has proved its worth to the extent that two such stations are used, one before the field is impregnated, and the other just before the field is assembled into the final machine.



(B)

At the first station, that is, before the impregnation treatment, immediately after the current readings balance are taken and with current still flowing, a polarity check of the windings is made by use of a compass.

Variations Affecting Temperature Rise

In a forced-cooled aircraft generator practically all of the losses are carried away by the cooling air. It is, therefore, of paramount importance to obtain the maximum possible amount of cooling air through the right passages and to have this air wipe the maximum area of winding surface.

The General Electric type *P*-1 generator has been designed with several ducts through the field and with the maximum area of winding surface exposed to the cooling air. These ducts result naturally when the shunt and commutating coils occupy their allotted space. In order to maintain uniform air ducts through the fields, it is necessary to shape the shunt coils and to compress and mold them to a definite size. To do this, these coils after being wound on standard winding machines are impregnated in a molding varnish, pressed and baked in heated molds as shown in Figure 7.

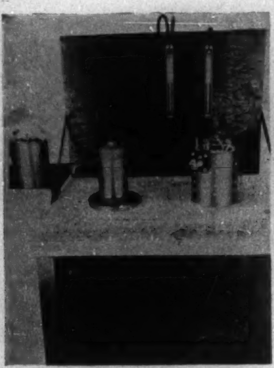


Figure 10. Field air-flow measuring instrument

Coils, when removed from the press and while still hot, are checked for short circuits on the "short-circuit tester" shown also in Figure 7. This device is a sensitive bridge circuit operating on 60 cycle power. A short circuit in the coil unbalances the magnetic field causing the instrument needle to deflect.

Figure 8 indicates still another checking station, this time a "turn counter." Shunt field coils which have passed the "short-circuit tester" are checked for proper number of turns by balancing the voltage generated in the coil undergoing test to that of a standard coil. The instrument is marked off to permit coils to pass with plus or minus one turn from the standard.

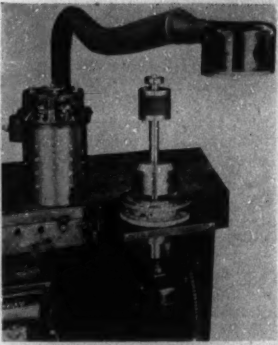


Figure 11. Brush-seating machine

Figure 12. Motor run-in stands

Commutating coils are wound in what is termed a "back turn winding" directly onto the insulated pole. These wound poles are then placed between hot pressing plates, pressed to definite dimensions, and baked to set up the special thermosetting varnish on the glass covered wire.

Figures 9*A* and 9*B* show shunt and commutating coils before and after molding. Note also the portion of the commutating pole projecting beyond the winding. This is the frame side of the pole, and when the pole is assembled into the magnet frame, an air duct is obtained between the frame and the under side of the commutating pole winding. Such molded shunt and commutating coils also provide an air duct between the shunt and commutating coils.

Manufacturing variations may result in too little air flow through the field. A "field-air-flow measuring instrument" as shown in Figure 10 was therefore designed in order to check the air passages through the field. The instrument is located in the production line at an early stage in the manufacturing cycle, where a field can be easily repaired if it has been rejected for restricted air passages.

The "air-flow instrument" consists of a blower which forces the air through an air-flow-measuring box shown under the table. A short pipe connects the exhaust of the box to the flange on which the field assembly is placed. A dummy armature forces the air to go through the passages in the field.

To make use of this instrument, a field assembly to be checked is placed over the dummy armature and rested upon the flange. A static pressure of 2.5 inches of water, measured in the pipe just below the flange, is set, and the differential

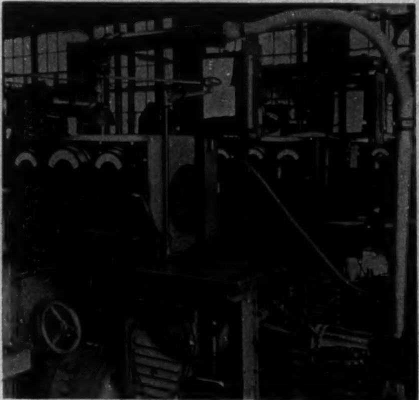


Figure 13. Generator test stands

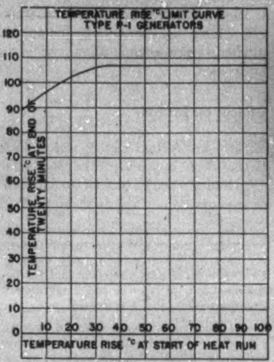


Figure 14. Shunt-field temperature-rise curve

pressure across the orifice in the airflow box is read. This reading determines the air flow through the field.

Tests have shown that by means of this device, the temperature rise of any field can be quite accurately predicted.

Variations in the Assembled Generator

The assembled generator combines all the variations and tolerances of its individual parts. It is only by diligently and resourcefully controlling the variations of these component parts that consistent generators are manufactured.

The assembly and final test of the completed generators bring out the inconsistencies and reject those generators which do not meet specification requirements. Here again, however, variations arise in the tests themselves. Testing variations can be controlled by:

1. Proper supervision and instruction of the testers.
2. Proper instrumentation.
3. Proper brush fit and correct brush setting.

Figure 11 shows a "brush-seating machine" which is a very definite aid in obtaining excellent brush fits in a short time. Brushes as received from the vendor are ground to approximately the correct radius. The brush rigging with the brushes in place is assembled with the field and endshield, all screws are tightened, and the brush rigging set in approximately the correct position. The "brush-seating machine" is then used to obtain a nearly perfect brush fit. The combined assembly; field, brush rigging, and endshield is placed over the arbor. The brushes are placed in their respective holders onto the grinding arbor, and held in their normal position by the brush springs. The exhaust hood is dropped over the endshield, the motor started, and allowed to drive the grinding arbor in the normal direction of rotation for approximately 15 seconds. At the end of this time, the brushes are properly fitted and very nearly polished.

Figure 15. Vibration test stand

The abrasive for this fixture is crocus cloth. The diameter over the crocus cloth is equal to the nominal diameter of the commutators. Very little of the brush is removed during this operation, the maximum amount ever experienced being not over 0.010 inch.

Figure 12 shows a pair of motor run-in stands where the generators are run as motors for approximately five minutes to perfect the brush fit. Occasionally a machine requires the application of a soft "brush-seater" to obtain the required brush fit. Here, also, electrical or so-called "inductive neutral" is located, and the brush rigging set in this position. The generator is then sent on to the final test.

Final Test

Figure 13 shows a group of test stands where the generator receives its final tests. These final tests consist of the following:

1. Setting of final brush position.
2. Base-speed heat run.
3. Regulation at base and top speeds.
4. Commutation check at top speed, full load.
5. Overspeed test.
6. Repeat of number 4.
7. Vibration reading—generator run as a motor.
8. High potential test.

Because of certain unique features, two of the tests listed will be discussed briefly.

Base-Speed Heat Run

All generators are subjected to a base-speed full-load heat run, the duration of which is either 20 minutes or until temperatures and field current readings have stabilized. Test limits for 20 minute runs have been established whereby a generator having a temperature rise of X degrees centigrade at the start of the heat run must not exceed Y degrees centigrade temperature rise at the end of 20 minutes, or more than a certain value of field current; otherwise, the heat run must be continued to the stabilization point. These limits of temperature rise for 20 minute heat runs are shown in the form of a curve in Figure 14. This curve is the result of a large number of tests and assures that any generator meeting the 20 minute heat run limits will meet the limits for ultimate heat runs.

To obtain consistent and accurate heat run data, each test stand is equipped with identical blast tubes and covers as shown in Figure 13. All voltmeters, ammeters, air-pressure gauges, and speed tachometers are carefully checked, and recalibrated, if necessary, at least once a week.

Vibration

Vibration or balance readings are taken on each generator after the completion of tests. With the generator mounted in the "vibration stand" as shown in Figure 15 and running as a motor, actual vibration readings are taken at each end of the generator by means of an electric vibrometer.

The vibration stand itself consists of a somewhat flexible steel table on which is mounted a heavy steel plate insulated from the table top by means of sponge rubber pads. The generator to be tested is mounted on a felt pad in a "V" shaped wooden block which in turn is insulated from the steel plate by means of sponge rubber pads. The electric vibrometer is mounted directly on a heavy steel block. The design and construction of this equipment eliminates external vibrations and well insulates the generator from the vibrometer.

Conclusion

Certain devices and means have been shown which are definite aids to quality control in the mauufacture of aircraft generators. New problems will require new aids. This is but one of the many tasks of the engineer today.

Behavior Factors of Rectifier-Driven D-C Motors

VICTOR SIEGFRIED
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Synopsis: In the rapid development and application of rectifiers to drive d-c motors, certain factors which influence the behavior of the motor may require unaccustomed consideration. The pulsating nature of currents which may be encountered introduces departures from conventional treatment of (1) heating value of the current waves, (2) stresses upon the insulation and commutator resulting from high peaks of voltage involved, and (3) torque pulsations tending to produce vibration and noise.

Analysis is made from experimental and theoretical approaches of possible ratios of the heating values to be expected with typical wave shapes applied to electronically driven motors. The ratio of effective to average values of current varies with the load on the machine and with the number of tubes used in the rectification process. It is shown that the problem is most acute in machines of small rating where mechanical inertia is low and where tube capacities would permit the use of but one or two tubes for economy of materials. This situation may require "overmotoring" an application in order to allow the required thermal capacity. Vibrations which will otherwise show up in undesirable effects on the driven machine, and noise, would also dictate the use of larger machines than necessary with conventional d-c practice.

The conclusion is drawn that factors not at first apparent must be considered in the application of the rectifier drive, and that as many phases as practicable are desirable in the rectifier.

THE electronic rectifier is currently achieving considerable prominence as a source for d-c motors.[1–5] It has many advantages over both fixed- and variable-voltage generator systems in the reduction of critical materials and elimination of rotating parts in the conversion process. Furthermore, its ease of control by means of the grid-phase-shifting system of the thyratron or the ignitron suggests many applications where new results can be secured.

In the use of phase-controlled rectifier sources for d-c motors, considerable attention must be given to the wave shapes of the currents supplied to the machine and their effects on it. Whereas continuous and direct currents have long been considered synonymous, there is introduced a new concept of *direct* current which may be pulsating but still continuous or even may be discontinuous for large fractions of a cycle. These differences from the continuous and substantially steady values encountered in conventional d-c practice produce far-reaching and sometimes surprising effects. They show that the rectifier is not inherently a panacea for all ills, but rather introduces problems of its own which can be met and conquered if recognized.

Nature of Problems

The controlled rectifier is inherently a phase-angle-control device which delays the beginning of current flow to a predetermined point in the cycle of alternating voltage applied to the tubes. For single-tube half-wave rectifiers, the result is pulses of current which can never be continuous but will always be less than one-half cycle in length. With two tubes in a full-wave rectifier, the current produced will be pulsating but seldom can be continuous. In polyphase systems with three or four tubes the voltages will overlap and the currents can become continuous when the rectifier is turned full on. With six or more phases, the possibility is reduced that phase control will produce discontinuous currents for any but the lowest of output levels. With sufficient number of tubes, the current pulses of the individual tubes are smoothed out, and the resultant current approaches the steady value of generator-produced current.

Thus the region in which these problems are encountered is confined to rectifiers using only a few tubes and in which the output is either discontinuous or else contains an appreciable ripple component.

There are a number of methods, inherently expensive, for filtering this output to make it smooth by the time it reaches the motor. Combinations of inductance and capacitance are required. Inductance in the machine may help as in the field circuit, where the machine is somewhat self-filtering. In the armature circuit, however, a high inductance interferes with commutation and is to be avoided.

Effects on Motor

There are certain factors which must be considered in analyzing the behavior of d-c motors when subjected to currents of this type.

1. The current produced by the rectifier is pulsating. It may be continuous but pulsating to a high degree, or it may even be zero for a portion of the cycle and be discontinuous, which is the more aggravated situation.

2. In order to achieve a required average value of current, this pulsating current must have peaks which are much larger in value. This results in an effective or root-mean-square current much larger than indicated by d-c ammeters.

3. The heating value of these pulsating waves is much greater than for the equivalent amount of conventional direct current. The resistance takes on an effective value which is larger due to stray-load-loss components, and the high peaks of current involved thus make the total I^2R loss far greater than ordinarily anticipated.

4. Peaks of voltage in the armature will be encountered due to the interaction of the pulsating current and the inductance of the armature and anode transformer circuits. These give a voltage wave shape in which the d-c value is almost unrecognizable, with peaks three or four times as large as the voltmeter value. These voltages endanger the armature and commutator insulations, and in some cases, commutator sparking is a direct consequence of the instantaneous peaks.

5. Torque produced by the machine becomes pulsating, with its attendant noise and vibration problems. These may be reflected in unsatisfactory performance of the driven machine, especially at light load or low speeds.

Analysis of Current Output

With current wave shapes as variable as occur in these rectifier drives, no one method of attack fits all cases. Where current is continuous but pulsating, it may be analyzed much as an average value with superimposed ripples of high frequency. The fundamental of this harmonic will be the supply frequency times the number of phases involved.

For discontinuous waves, the effect on the machine is more serious. Fortunately the analysis is quite simple due to the fact that the armature current pulse approximates a half sine wave, regardless of the portion of the cycle which it occupies. This is due to the influence of the inductance of the circuit, tending to hold the current rise down, and main-

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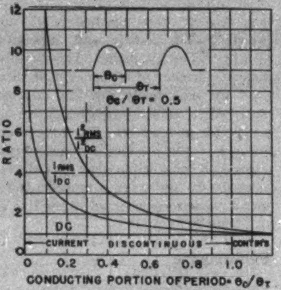

Figure 1. Effect of angle of conduction on heating and current ratios

Insert: Example of wave shape when $\theta_C = 0.5\theta_T$

tain current flow past the voltage zero. Vedder and Puchlowski[4] have given good detailed analyses of this, but for purposes of this paper, the approximation to the half sine wave is adequate. In the following analysis, it is to be re-emphasized that the range in which more serious differences occur is at light loads and low speeds, depending on specific applications of the drive.

The main comparison of heating values, the most important difference, is in that of the average and effective values. The peak value of current, I_m, is used as the comparison figure in each case. For any given sine-wave pulse of half-cycle value as in Figure 1, the average value over that time is $2I_m/\pi = 0.636\, I_m$ and the effective value is $0.707\, I_m$. In averaging the current over a complete cycle, it will be in proportion to the ratio of angle of conduction, θ_C, to the total cycle, θ_T, but the effective value will be the square root of the average square. For the case shown in Figure 1 where the current flows for one half the time, the effective value will be $0.5\, I_m$, whereas the average (d-c) value will be $0.318\, I_m$, a ratio of 1.57 to 1. Thus the true heating I^2R value of the wave will be $0.25\, I_m^2$ whereas that indicated by a d-c instrument will be $(0.318 I_m)^2$ or $0.101\, I_m^2$, a ratio of $2^1/_2$ to 1. In the case of current for one-third of a cycle, the ratio is even more striking, as the heating rate is 3.7 times that of the equivalent smooth direct current. Relationships for other conduction periods are shown in Table I and Figure 1. These also show that for the limiting case of multiphase operation, these ratios become 1.0, and average and effective values are equal.

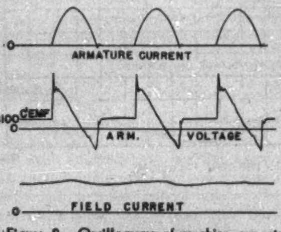

Figure 2. Oscillograms of machine currents and voltage for $\theta_C = 0.53\, \theta_T$

Armature current = 1.55 amperes root-mean-square and 1.09 amperes direct current

Armature voltage = 147 volts root-mean-square and 100 volts direct current

Field current = 0.360 amperes root-mean-square and 0.340 amperes direct current

Table I. Effective and Average Current Ratios for Discontinuous Currents

Conducting Portion of Period, θ_C/θ_T	D-C (Average) Values		Root-Mean-Square (Effective) Values		Current Ratio, I_{RMS}/I_{DC}	Heating Ratio, I_{RMS}^2/I_{DC}^2
	$\times I_{max}$	$\times I_{max}^2$	$\times I_{max}$	$\times I_{max}^2$		
0.10	0.064	0.004	0.223	0.050	3.49	12.5
0.25	0.159	0.025	0.354	0.125	2.24	5.0
0.33	0.212	0.045	0.407	0.166	1.92	3.70
0.50	0.318	0.101	0.500	0.250	1.57	2.49
0.67	0.424	0.180	0.577	0.333	1.33	1.85
0.75	0.477	0.228	0.612	0.375	1.28	1.65
0.90	0.572	0.327	0.671	0.450	1.17	1.38
1.00	0.636	0.405	0.707	0.500	1.11	1.23
Multiphase	1.0	1.0	1.0	1.0	1.0	1.0

Note: Table based on $I_{max} = 1.0$, and $R_{eff} = R_{d-c}$. Specific application of these data to rectifier-driven motors requires determination of actual current wave shape for each load and operating speed adjustment.

A typical oscillogram of voltage and currents in an actual full-wave rectifier-driven machine is shown in Figure 2. Conduction is for 53 per cent of the half cycle. The values of effective and average values are shown, and agreement is quite close to the anticipated values from Figure 1 for this setting of the control. This indicates that the sinusoidal approximation is in quite close agreement to actual conditions.

Such an example will apply equally well to any number of phases in a rectifier if the conduction period is one half of the portion of the cycle assigned to each phase. Thus, the case shown would be like a half-wave circuit conducting 180 degrees per anode, or a full-wave circuit conducting 90 degrees per anode, or a three-phase circuit conducting 60 degrees per anode, and so forth.

Results of Analysis

When, as in the examples shown, current is operating in the discontinuous range, the motor heating rate due to copper loss alone will be excessively large for the intended rating. The remedy would appear to be to figure the duty cycle carefully to include these effects, or simply allow a step-up in the motor size to provide a safety factor. The obvious alternative is to allow the motor to run hotter and hope that the life of the insulation will be satisfactory.

Another factor which contributes to the machine's running hotter is the core loss. While the current may be self-smoothing to a large degree, there is a high-frequency flux pulsation which will occur where there is usually no impediment to eddy-current losses. This would indicate the desirability of laminated core and pole pieces.

Two other factors which combine against the life of the insulation are vibration and voltage stresses. The probable life of the insulation seems doomed to be shorter than for conventional motors regardless of heating. The combination of excessive voltage strains, chafing under vibration conditions and higher temperatures indicates that much more or better insulation must be built into the machine's design.

The remedy really lies in careful engineering of an application. The load can be figured to keep the motor working in the continuous current portion of the rectifier operation. Multiphase rectifiers can be used, so as to approximate the steady current of a generator as nearly as economically feasible.

Conclusions

This analysis of the application of phase-shift-controlled rectifiers to d-c motors leads to these conclusions:

1. Consideration must be given factors not usually encountered in d-c machine practice.
2. These factors are most acute in machines of small rating where the mechanical inertia is low and the load requirements justify the use of only one or two tubes.
3. Heating of the motor will be increased by the use of rectified currents over conventional direct current, especially if machine currents are discontinuous.
4. Vibration will be especially prominent at low speeds.
5. Excess heating and vibration become of

A Unit for Balancing the Voltages of Three-Phase Aircraft Inverters Supplying Large Single-Phase Loads

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Synopsis: A means of reflecting a single-phase load so it appears as a balanced three-phase load to the power supply is discussed in this paper. A balancer of this type, designed for use in aircraft, is described and its characteristics discussed.

Background

THE increase in electrically powered and electrically controlled equipment on aircraft has made it necessary, in certain cases, to add sizable single-phase loads to three-phase inverters. The nature of the existing three-phase load, however, placed severe restrictions on the permissible voltage unbalance. The need was felt for a means of maintaining balanced voltages between phases of a three-phase inverter operating under the conditions described above. As this balancer was to be used on aircraft, the further limitations that it be light in weight and efficient in operation were imposed.

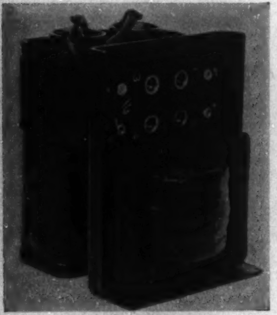

Figure 1. The unit described in this paper rated 265 volt-amperes, 115 volts, 94 per cent power factor, 400 cycles

In February 1943 a request was made by the Army Air Forces at Wright Field for a balancer of this nature. The single-phase load specified was approximately 265 volt-amperes, 115 volts, 94 per cent power factor, 400 cycles. The power supply was a three-phase inverter rated at 750 volt-amperes, 115 volts, 400 cycles.

The Balancer

Several methods of attaining the desired results suggested themselves. The one decided upon[1] resulted in a unit weighing 1.5 pounds and measuring $3^1/_2$ by $2^3/_4$ by $2^3/_4$ inches. This balancer has an efficiency of 96 per cent at rated load and will supply this load continuously with a temperature rise of 40 degrees centigrade at sea level. The voltage unbalance of the inverter at rated single-phase load is negligible when the balancer is used. It consists of a tapped reactor and a capacitor connected to the line as in Figure 2.

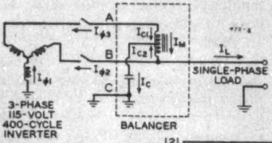

Figure 2. Schematic connection diagram for the balancer unit

Performance

The three-to-one phase balancer is designed for a particular single-phase load and will function ideally only at this load. Figure 3 illustrates this point. This characteristic of the balancer makes it advisable to incorporate the unit as part of the load and connect it to the power supply when the single-phase load is applied.

All of the accompanying data were taken using a Holtzer-Cabot inverter having the rating mentioned above for the three-phase output circuit. The inverter used had an initial voltage un-

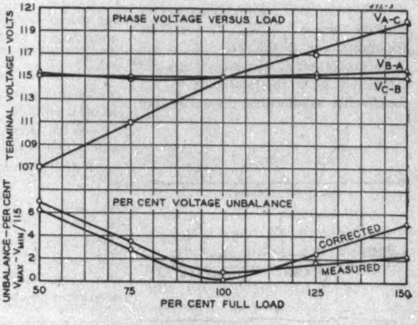

Figure 3. Voltage unbalance of the power supply under conditions of varying single-phase load. Balancer unit connected

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smaller importance as the number of tubes (phases) in the rectifier is increased.

6. Multiphase rectifiers appear to have definite electrical and mechanical advantages which will offset their increased cost and size.

7. Designs of machines must be altered to take account of current, torque, and flux pulsations superimposed on conventional d-c values.

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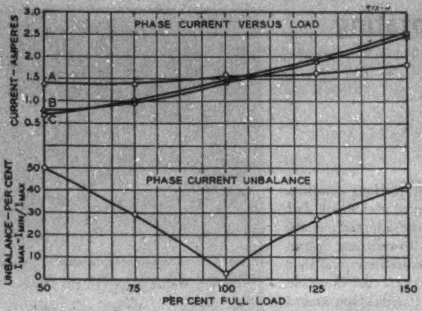
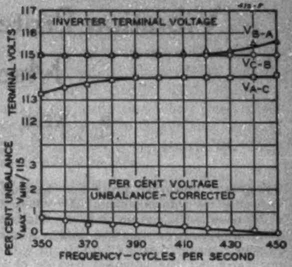
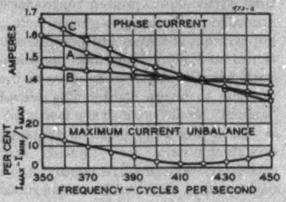


Figure 4 (left). Phase-current unbalance of the power supply under conditions of varying single-phase load. Balancer unit connected

Figure 5 (right). Voltage unbalance of the power supply versus frequency. Balancer unit connected

Figure 6 (below). Phase-current unbalance of the power supply versus frequency. Balancer unit connected





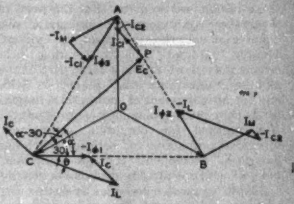


Figure 7. Vector diagram of the unit

balance of 0.8 volt at no load. The curves marked "corrected" have been compensated for this unbalance.

Since the unbalance of terminal voltage with unbalanced load is a function of the inverter, curves of phase-current unbalance have also been plotted to give a clearer picture of the behavior of the balancer.

The voltage across the load was kept constant at 115 volts so that the data would not be confused by the regulation of the inverter.

For the purpose of comparison, the performance of the inverter alone is given. At 100 per cent single-phase load on terminals B and C, with no balancer, the inverter voltages were unbalanced 7.65 per cent. The readings were:

V_{C-B}	V_{B-A}	V_{A-C}
115.0 volts	116.0 volts	123.8 volts

The frequency response of the balancer is also shown (Figures 5 and 6). It can be seen from these curves that the unit operates reasonably well over a wide range of frequencies, so that the value of the unit is not impaired by the speed regulation of the power supply.

Conclusion

The unit described is suitable for aircraft use due to its small size, light weight, and high efficiency. It has the added advantage of improving the power factor in the lines to 0.866 in those cases where the single-phase load is less than this value. Additional tests prove the unit will function at temperatures and pressures encountered at high altitudes.

Appendix

The operation of the balancer can be understood by referring to the vector diagram of Figure 7. The single-phase load is connected across terminals B and C of the inverter. The balancer is connected so that the reactor is across phases A and B and the free end of the capacitor is connected to terminal C. Phase rotation is A-B-C.

The value of the capacitor and the reactor needed to balance any single-phase load as well as the position of the reactor tap, can be obtained by referring to the schematic wiring diagram (Figure 2) and the vector diagram (Figure 7). The vector diagram is drawn to represent balanced conditions.

From Figure 2:

$$I_{\phi 1}=I_L+I_C \tag{1}$$

$$I_{\phi 2}=I_M-I_{C2}-I_L \tag{2}$$

$$I_{\phi 3}=-I_{C1}-I_M \tag{3}$$

Under balanced conditions

$$I_{\phi 1}=I_{\phi 2}\underline{/120^\circ}=I_{\phi 3}\overline{/120^\circ} \tag{4}$$

and

$$V_{OC}=V_{OB}\underline{/120^\circ}=V_{OA}\overline{/120^\circ} \tag{5}$$

Assuming no power lost in the balancer and the phase currents in phase with the terminal-to-terminal voltages (a condition found necessary to obtain balance), the power supplied by the inverter must equal the power absorbed by the single-phase load.

$$3E_\phi I_\phi \cos 30^\circ=E_L I_L \cos\theta \tag{6}$$

or

$$\sqrt{3}E_L I_\phi \times 0.866=E_L I_L \cos\theta \tag{7}$$

and

$$I_\phi={}^2/_3 I_L \cos\theta \tag{8}$$

From equation 1

$$I_C=I_{\phi 1}-I_L \tag{9}$$

The angle of the voltage across the capacitor is assumed 90 degrees lagging the capacitor current. The absolute value of capacitor voltage is:

$$/E_{CP}/=(115 \sin 60^\circ)/\sin(60+\alpha) \tag{10}$$

and the voltage E_{BP} across the lower part of the reactor is

$$/E_{BP}/=(115 \sin\alpha)/\sin(60+\alpha) \tag{11}$$

Making use of the superposition theorem, the net current through the reactor can be considered as being made up of the reactor magnetizing current I_M, a part of the capacitor current I_{C2} flowing between the reactor tap and terminal B, and the remainder of the capacitor current I_{C1} flowing between the tap and terminal A. It can further be seen that if the internal impedance of the power supply is small compared to the impedance of the load

$$I_{C1}/I_{C2}=E_{BP}/E_{PA} \tag{12}$$

The reactor magnetizing current is, from equation 2,

$$I_M=I_L+I_{C2}+I_\phi \tag{13}$$

The balancer described in the beginning of this article consisted of a four-microfarad capacitor and a reactor drawing a magnetizing current of 1.41 amperes tapped at 33.6 volts from the A terminal.

Reference

1. Letters to editor, *Electrical World*, December 26, 1942.

Vibration Protection for Rotating Machinery

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Synopsis: Vibration protection becomes desirable for rotating machinery when it is operated with certain defects. Electric generators may be operated with a single "ground" on the rotating field circuit without harm but the potential hazards of a second "ground" at another point on the same circuit are so great that the need of protective equipment for automatic disconnection of the generator is evident. Double "grounds" on a rotating field circuit may short-circuit a large part of the winding, unbalance the magnetic field, and set up vibrations of such serious magnitudes that the machine may be wrecked.

A method for obtaining satisfactory vibration protection for rotating machines has been developed. It uses equipment most of which is already on hand or readily available to most companies operating important machinery. The only part requiring construction is an electronic amplifier.

The equipment, its operating characteristics, wiring, and performance are described in this paper.

The described protective equipment has been in service on one machine since the latter part of 1942. It has given entirely satisfactory service. Another machine was protected in a similar manner for about three months until it was found convenient to rewind the field.

ROTATING machinery of all types is subject to damage when exposed to severe vibration caused by mechanical unbalance, or its equivalent, in the rotating element. Although electrical machinery has a good record in this respect, its freedom from vibration has been due largely to the degree of maintenance which it usually receives. Now, however, due to wartime restrictions, electrical and other machinery is often run under conditions which would not be permitted normally until after repairs had been made. When this is done, protective measures should be adopted where possible to prevent serious machine damages if the weakened part should fail.

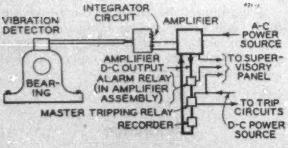

Figure 1. Schematic arrangement of vibration protection equipment

One expedient being adopted, as the occasion demands, is to continue operating a-c synchronous machinery after a "ground" has developed in the field winding of the rotor. Since the excitation system is operated ungrounded normally, the winding-to-ground fault is not harmful and does not prevent continued and normal machine operation. Such operation does, however, expose the machine to severe damage if a second "ground" should occur in the field winding or at any other place on the same excitation system. Two "grounds" may effectively short-circuit a portion of the field winding, thereby causing magnetic unbalance in the rotor. If a large portion of the field winding is short-circuited by the second "ground," the field current in the remainder of the winding will increase and further aggravate the unbalanced magnetic condition. Cases are on record of double "grounds" in field windings which have caused such severe vibration that the bearing pedestals were broken allowing the rotating field to drop on the stator laminations. Such failures have, in each known case, caused damages which required costly repairs and long machine outages.

A protective method, that detects excessive vibration, has been developed by engineers of the Consolidated Edison Company of New York. It operates relays which function quickly to initiate the circuit breaker operations required to relieve the condition. In the case of synchronous machinery the a-c armature and d-c field breakers are opened as fast as possible and, if desired, the prime mover power may be shut off. Other types of machinery would be handled as conditions and equipment permit.

Protective System

The protective system consists of the following parts:

1. Seismographic type vibration detector.
2. Integrator circuit.
3. Resistance coupled amplifier.
4. Alarm and protective relays.
5. Vibration recorder.
6. Supervisory circuits.

Figure 2. Seismographic vibration detector with cover removed

The arrangement of these parts is shown schematically in Figure 1. An alternating voltage is generated by the vibration detector when vibration is present in the part on which it is mounted. This voltage is impressed, through an integrator circuit, to an amplifier. The current is amplified and then rectified before being passed through the relays and the recorder. The system is provided with various features which prevent incorrect performance of the relays due to wire failure, tube failure, and loss of a-c or d-c power supply.

Vibration Detector

The vibration detector was one originally furnished by its manufacturer for use in connection with vibration recorders. It is constructed on the seismograph principle, with a strong permanent magnet supported on springs serving as the stationary element. A light coil supported to the base of the detector is free to move in the magnetic field when the base of the detector is subjected to vibration. The base of the detector is mounted rigidly on the bearing or other part whose vibration is to be measured. The detector is inherently a velocity measuring device since the alternating voltage it generates is directly proportional to vibration frequency and to vibration amplitude for sinusoidal vibration.

The detector is shown without its cover in Figure 2. Figure 3 shows two detectors mounted on a generator-outboard-bearing cap. Generator-bearing pedestals

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Figure 3. Vibration detectors mounted on turbogenerator bearing

are frequently insulated from the supporting bedplate of the machine. Care should therefore be exercised to prevent short-circuiting this insulation when installing metallic conduit for connection to the detector device mounted on the bearing cap. The detector mounting must be rigid enough to avoid resonant vibration in the detector supports. Since the wiring from the detector to the amplifier requires protection against induced voltages, twisted and shielded conductors are used. It is also preferable to have them routed remotely from other circuits carrying alternating current or impulse currents.

Integrator Circuit

Since it is desired to operate the protective relay on vibration amplitude only, it is necessary to interpose an integrator circuit between the detector and the amplifier. On the installation being described, this circuit consists of a 19,000-ohm resistor in series with a three-microfarad capacitor. The output from the detector is connected across the complete circuit while the voltage drop across the capacitor is wired to the amplifier and is used to govern the operation of the relays. The resistance of the circuit predominates over the capacitance, except at very low frequencies, hence the current in the circuit remains nearly constant with the same impressed voltage for any frequency of vibration at which protection is desired. Since for a given vibration amplitude the voltage generated in the detector is directly proportional to frequency while the capacitive reactance of the capacitor is inversely proportional to frequency it follows that the voltage drop across the capacitor does not change with different frequencies and is therefore proportional to vibration amplitude alone. The output voltage E_2 of the integrator circuit may be calculated for any input voltage E_1 and frequency f by use of the formula:

$$E_2=\frac{E_1}{(19{,}000^2+X^2_c)^{1/2}}$$

where

$$X_c=\frac{10^6}{2\pi f\times 3}$$

The circuit produces a voltage output nearly proportional to vibration amplitude over a wide frequency range. One curve of Figure 4 shows the degree of accuracy obtained with the circuit. At 30 cycles per second and above the error is negligible. At five cycles per second the output of the circuit is about 12 per cent low. Five cycles per second corresponds to about 16 per cent of rated speed for this machine.

Amplifier

The amplifier is resistance coupled so that it also will provide good accuracy over a wide frequency range, and particularly at the lower vibration frequencies. The degree of accuracy obtained in the amplifier at different frequencies is shown by the other curve of Figure 4. Reasonably good accuracy at low frequencies is desired to provide satisfactory vibration protection while machines are being brought up to speed or slowed down after a run. An over-all error of 15 per cent at five cycles per second, 12 per cent for the integrator circuit, and 3 per cent for the amplifier, is considered satisfactory for the protective relays since damage due to vibration is not likely to be severe at lower speeds. Some generators, such as cross-compound units, must have their field windings energized at the time of start-up and would, therefore, be subject to vibration due to double field "grounds" even at moderate speeds.

The over-all voltage amplification of the amplifier is about 1,700. Its response is linear from zero to 16 millivolts input from the integrator. The d-c load circuit resistance is 1,700 ohms. At 20 milliamperes, the output of the amplifier is 34 volts direct current and 0.68 watt.

Three stages are used in the amplifying circuit, two stages being obtained in two double-triode tubes and a third stage being obtained in two triode tubes connected in push-pull arrangement. To prevent incorrect relay operation in case of electron-tube failure, the filament circuits of the various stages are connected in series with an instantaneous overcurrent relay. When the filament current is reduced due to any filament failure the relay operates to open the output circuit of the amplifier and to close a signal circuit. Such operation makes the protective relays inoperative and starts a supervisory lamp flashing at a point where it will attract the attention of the operators.

The amplifier output is rectified by a copper-oxide bridge circuit and the direct current from the rectifier is wired to the relays, and to the recorder, if used.

Figure 5 shows a schematic diagram of the amplifier connections and associated equipment. The over-all calibration of the vibration-protection equipment including the detector unit, integrator, and amplifier is given in Figure 6. To obtain this calibration initially the detector was caused to vibrate and its double amplitude of vibration in mils was measured with a portable Starrett type vibrometer. The range of d-c output current as obtained from the amplifier for various values of vibration is entirely suitable for relay application. It is not practicable to check the over-all calibration (d-c output current versus mils vibration) each time the amplifier and relay equipment require calibrating. Since there are occasions when the amplifier must be recalibrated (when tubes are changed or when other parts are replaced) a 60-cycle calibration curve was obtained following the initial over-all calibration. This curve is also given in Figure 6. The calibration curve was obtained by impressing 60-cycle voltages across the same input terminals to which the output of the detector is connected. This calibrating curve assists not only with adjustments to the amplifier and relays but permits a complete check on the accuracy of the recorder, as required.

It will be noted on Figure 5 that the amplifier circuits are grounded through a 0.5-megohm resistor. This resistance is intended to prevent incorrect operation of the relays in case a wire "ground" should develop in the detector output circuit at a point where the "ground" potential is operating at a small voltage difference

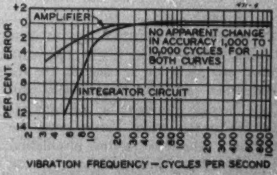

Figure 4. Accuracy curves for integrator and amplifier versus vibration frequency

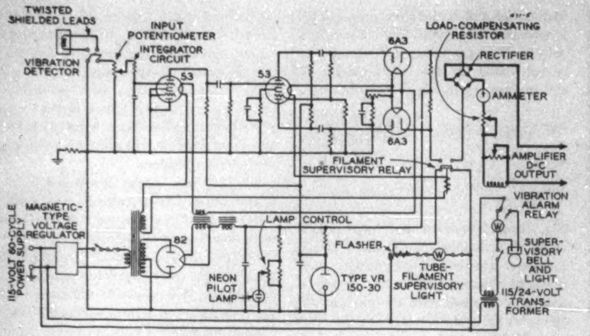
TWISTED SHIELDED LEADS
VIBRATION DETECTOR
INPUT POTENTIOMETER
INTEGRATOR CIRCUIT
53
53
6A3
6A3
LOAD-COMPENSATING RESISTOR
RECTIFIER
AMMETER
AMPLIFIER D-C OUTPUT
FILAMENT SUPERVISORY RELAY
VIBRATION ALARM RELAY
MAGNETIC-TYPE VOLTAGE REGULATOR
115-VOLT 60-CYCLE POWER SUPPLY
82
LAMP CONTROL
FLASHER
NEON PILOT LAMP
TYPE VR 150-30
TUBE-FILAMENT SUPERVISORY LIGHT
W
SUPER-VISORY BELL AND LIGHT
115/24-VOLT TRANS-FORMER

Figure 5. Connection diagram for amplifier and associated equipment

with respect to the "ground" bus potential at the amplifier. A potential of about one volt at 60 cycles between the two wires from the detector is sufficient to cause operation of the master tripping relay.

Relays

A diagram of relay connections is given on Figure 7. Two "lock-out" relays are used, one being an a-c undervoltage relay and the other a d-c undervoltage relay. These undervoltage "lock-out" relays open the tripping circuit instantly if either source of power is lost. The a-c relay will reset about one minute after restoration of voltage which allows ample time for the tubes of the amplifier to become heated and its output current to become steady before the equipment is returned to service. The d-c undervoltage relay resets in about 10 seconds after voltage is restored, this time allowing for the master tripping relay element to come to its stable position after its field coil is energized.

The alarm overcurrent relay is a simple hinged armature device equipped with a variable resistor connected in parallel with its coil to provide means for adjusting its operating current value. A second resistor is connected in series with the relay circuit to permit readjustment of the over-all series-circuit resistance when any variation is made in the resistance in parallel with this relay coil. This permits retaining a constant resistance in the output circuits from the rectifier. The relay for this installation is adjusted to give an alarm to the operators at eight mils vibration.

The master tripping relay has a standard d'Arsonval type element with a field coil. Its field coil is connected to a separate d-c power supply. The d-c output from the rectifier is passed through the moving coil of the relay. Since the moving element is restrained by a small spiral spring the element changes its position to follow any increase of vibration. Adjustment is made by changing the position of the stationary contact so as to require more or less travel of the moving element against the restraining spring. Since the relay moves to follow any change in vibration almost instantly, it will be in position to trip shortly after the machine vibration reaches the value for which the relay is set to operate. The master tripping relay trips the main generator breakers and the field breakers when the bearing vibration reaches 12 mils.

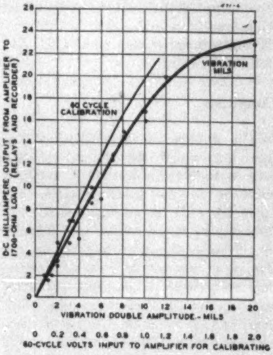

Figure 6. Characteristic output curves of amplifier

Any d-c relay which will operate over the range of current shown in Figure 6 would be suitable for the master tripping relay. Of course the vibration scale can be contracted or expanded on Figure 6 by simply varying the potentiometer adjustment at the input to the integrator. The relay used should be fast to prevent an excessive overswing of the vibration before the tripping relay operates.

The relays become inoperative but do not operate incorrectly due to the following troubles: (1) open-circuited or short-circuited wires from the vibration detector, (2) failure of electron tubes, (3) interruption of a-c or d-c power supply, (4) relay failure, or (5) tripping-circuit failure. Complete supervision is provided to the operators for conditions (2), (3), and (5).

Figure 8 shows the relay panel with the amplifier at the top. It will be noted that two complete protective equipments are provided. The two corresponding vibration detectors are shown in Figure 3. On this installation two complete protective equipments were installed with separate power sources and tripping circuits to provide a high degree of reliability. In case any part of one equipment develops trouble the second equipment will provide complete vibration

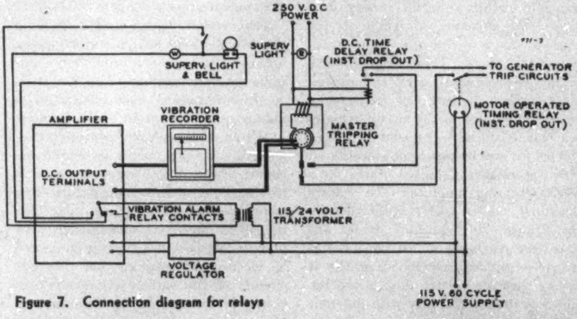
250 V. D.C POWER
SUPERV LIGHT
R
D.C. TIME DELAY RELAY (INST. DROP OUT)
W
SUPERV. LIGHT & BELL
TO GENERATOR TRIP CIRCUITS
MOTOR OPERATED TIMING RELAY (INST. DROP OUT)
AMPLIFIER
VIBRATION RECORDER
MASTER TRIPPING RELAY
D.C. OUTPUT TERMINALS
VIBRATION ALARM RELAY CONTACTS
115/24 VOLT TRANSFORMER
VOLTAGE REGULATOR
115 V. 60 CYCLE POWER SUPPLY

Figure 7. Connection diagram for relays

protection since all of its parts function independently. Since this particular generator is one of the largest on the system it was decided that the unit should not be run at any time without at least one vibration protective equipment in service. Two equipments have the advantages of permitting tube changes and calibration work on the parts of one without having to take the main generator out of service and they provide a continuous check on the accuracy of each other.

Vibration Recorder

A recorder may or may not be used in such a circuit but on the installation being described a recorder is found useful and it is inserted in series with the d-c output from the rectifier. The instrument is located near the turbine to permit continuous supervision of the bearing vibration by the turbine engineer. A continuous record is obtained of bearing vibration in a manner not feasible with indicating instruments. Samples of such records when starting, when running, and when shutting down are shown in Figure 9. The recorder instrument is an ordinary low range d-c milliammeter.

Supervisory Circuits

A supervisory panel for the equipment may be located at any convenient point. In the installation being described this panel is placed above the main control panel for the generator. There are four signals:

The first is an alarm and a light operated by the vibration alarm relay at 8 mils vibration.

The second signal is a light which is normally energized. It starts to flash when an electron-tube filament burns out. Such action is initiated by the undercurrent relay which is in series with the amplifier filament circuit. Loss of filament current makes the protection inoperative.

The third signal is a light which is normally energized and which becomes dark when the d-c power supply is interrupted. In this installation, the d-c power is used only for polarizing the master protective relay, hence, loss of that supply makes the protective equipment inoperative.

The fourth signal is the lights of the first and second signal both of which become dark when the a-c power supply is interrupted. Loss of a-c power deenergizes the amplifier and makes the protective equipment inoperative.

Figure 8. Vibration protection relay panels

Operating Experience

No uncontrolled or accidental outage of the equipment has occurred since it was placed in operation in October 1942. It has not been called upon to disconnect the generator automatically since no severe vibration has occurred.

On one occasion a momentary "ground" occurred on one of the wires that connect to the vibration detector. Since the "ground" potential at that point was slightly different from the "ground" potential where the amplifier was mounted, this voltage difference, being impressed on the first stage of the amplifier, caused a surge of direct current to the relays and recorder. The recorder swung up-scale and the vibration alarm relay operated but the "ground" did not remain on the wire long enough for the master tripping relay to operate. The 0.5-megohm resistor was then connected in the "ground" circuit at the amplifier to prevent incorrect tripping due to an accidental "ground."

When first installed the vibration alarm relay was adjusted to operate at six mils vibration. It was found that the relay would operate occasionally as the generator was passing through its critical speed on starting or shutting down operations. The relay was readjusted to operate at eight mils vibration and no further relay operations of this type have been reported.

On two or three occasions the vibration alarm relay has operated and reset as the generator load was being increased. It was found that the machine experienced abnormal vibration of short duration as some of the steam valves are opened to increase the power output of the turbine. This behavior does not occur each time the load is brought up on the machine, in fact its occurrence is more "the exception" than "the rule."

Maintenance

Maintenance on the vibration protective equipment may be made to fit in with maintenance on other generator protective relays. The amplifier, relays and recorder should be calibrated on the same schedule with generator differential relays.

The amplifier tubes should be replaced every three to six months assuming they are in service continuously. The replacement tubes for the push-pull stage should be selected for balance before being installed.

The vibration detector should be inspected at times when the machine is shut down to determine whether any parts have become loose; once a month is often enough.

The usual attention to paper and ink must be given the recorder.

Conclusions

1. A simple scheme is made available for suitable and fast vibration protection for rotating machinery.

2. The protective scheme provides suitable protection against magnetic unbalances as well as against mechanical unbalance in rotating elements.

3. The equipment described has been in service since 1942 and has given a record of reliability entirely satisfactory for large turbogenerator protection.

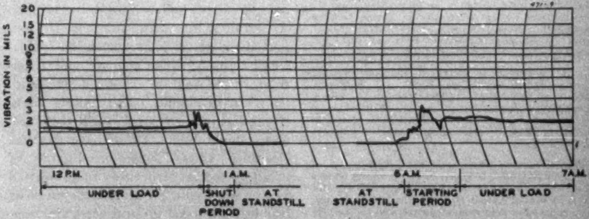

Figure 9. Typical vibration recorder charts for turbogenerator bearing

Measured Electrical Constants of 270-Mile 154-Kv Transmission Line

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Synopsis: Results of measurements are presented of fundamental electrical constants of a 270-mile 154-kv transmission line. Comparisons are made between values obtained from test and those obtained from circuit dimensions and computations, thus providing a check on some conventional methods of determining basic line impedance and admittance units. Included are unit resistance, inductive reactance, and capacitive susceptance together with ***ABCD*** constants and charactertistic impedance of positive-phase sequence, zero-phase sequence, and one-conductor-and-ground long-line networks.

PART of results obtained from measurement of electrical constants of long transmission line have been presented previously.[1] Interest shown in these measurements and request for additional information led to this more complete presentation of data and results of tests which will be of interest to engineers who follow long-transmission-line theory and practice, relay application, and fault studies.

Engineers desire to check theory behind development of formulas and constants by actual measurement. Thus theoretical procedures which may include many factors and assumptions might be replaced, if possible, by more simple methods. Check measurements of zero-phase-sequence impedances especially are desirable, because calculations for these include effect of indefinite factors, such as, average distance between shield wires and conductors, their average height above ground, effect of earth-resistivity and reactance, and variations in construction from the straight-line structure. To a lesser degree perhaps, a check of positive-sequence values is also desirable to determine importance of assumptions used in theoretical calculations.

Since new-type impedance relays described previously[2,3] were being applied, and since accurate basic data for determining normal and short-circuit conditions were desired for satisfactory operation and protection, measurements of unit values were made upon completion of line but before it was placed in operation. These data obtained from measurements, also provided information to check relay settings previously computed from theoretical sources.

Specific methods of determining line constants are not recommended and a comparison between methods of computation is not made, but rather the paper is intended to present simply results of field measurements and to compare these with values obtained through some conventional procedures.

Line Construction

Typical structure used on straight-line construction is shown in Figure 1. Figure 2 gives average spacing and configuration of conductors and shield wires. Horizontal distances are same as those at typical straight-line structure and vertical distances are assumed as average considering that sag of shield wires is less than sag of conductor. Line is 268.52 miles long without intermediate switching stations or taps. Ruling span is 600 feet. It is single-circuit of wood-pole H-frame construction with horizontal conductor spacing of 14 feet 6 inches. Conductor is 0.683 inch in diameter and is 250,000 circular mils of hollow hard-drawn copper. At straight-line structures, it is supported by ten 10-inch suspension insulators having $5^3/_4$-inch spacing, and 15 insulators are used on grounded steel supports and dead ends. Two $^3/_8$-inch high-strength galvanized-steel overhead ground wires shield line. These have vertical clearance above power conductors of about 10 feet at structures and about 18 feet at mid-span. A steel cross member supports overhead ground wires and connects these to both pole grounds at each structure. Pole ground is number 6 steel wire carried down pole and wrapped around base. There are no counterpoises on line. Shield wires are connected to station grounds at terminals. There are three complete transposition barrels in line.

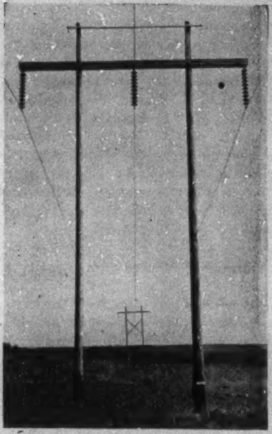

Figure 1. Typical structure, 154-kv line

Test Procedure and Conditions

Measurements were made by applying potential from 2,400/4,160-volt 60-cycle source supplied from three 50-kva 13,800–2,400/4,160-volt Y-connected transformers at one terminal with conductors at other end appropriately connected for six short- and open-circuit tests listed in Table I. Portable ammeters, voltmeters, and wattmeters of good quality and recent calibration were used with outdoor instrument-type current and potential transformers at terminal at which potential was applied. Similarly ammeters and voltmeters were used at opposite terminal. All measurements include impedance of carrier-current wave traps

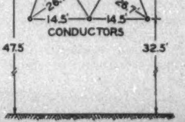

Figure 2. Conductor and shield-wire configuration. Approximate average spacings and heights above ground are shown

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Acknowledgment is made of the participation of L. E. Wallingford, formerly with Nebraska Power Company and now in the United States Navy, and W. A. Wolfe of Kansas Gas and Electric Company, who assisted in field measurements, and the helpful criticisms of D. M. MacGregor, Ebasco Services, Inc., who reviewed the paper.

Table I. Summary of Tests and Impedance Data

Test Description	Test	Measured Impedances in Ohms as Viewed From Instruments: Symbol	R	$+jX_L$ or $-jX_C$	Z/θ	Computed
Balanced three-phase potential applied to three conductors with them open at other end	*A*	$Z_{ocp}=\frac{A_p}{C_p}$	21.2	$-j648$	$648/\overline{88.13}$	$646/\overline{88.03}$
Balanced three-phase potential applied to three conductors with them shorted and grounded at other end	*B*	$Z_{scp}=\frac{B_p}{A_p}$	85.8	$+j243$	$258/\underline{70.6}$	$251/\underline{71.48}$
Single-phase potential applied between all three conductors and ground with them tied together and not grounded at other end	*C*	$\frac{Z_{oc0}}{3}=\frac{A_0}{3C_0}$	21.2	$-j273$	$275/\overline{85.6}$	$270/\overline{84.29}$
Single-phase potential applied between all three conductors and ground with them tied together and grounded at other end	*D*	$\frac{Z_{sc0}}{3}=\frac{B_0}{3A_0}$	88.2	$+j232$	$247/\underline{69.1}$	$275/\underline{64.39}$
Single-phase potential applied between one conductor and ground with it not grounded at other end	*E*	$Z_{oc1}=\frac{A_1}{C_1}$	34	$-j707$	$707/\overline{87.25}$	$695/\overline{86.53}$
Single-phase potential applied between one conductor and ground with it grounded at other end	*F*	$Z_{sc1}=\frac{B_1}{A_1}$	150	$+j399$	$426/\underline{69.4}$	$430/\underline{67.9}$

Table II. Positive-Phase-Sequence Constants

$A_pB_pC_pD_p$ Constants

	A_p	B_p	C_p
Measured	$0.853/\underline{3.19}$	$220/\underline{73.79}$ ohms	$0.001316/\underline{91.32}$
Computed	$0.854/\underline{2.84}$	$214/\underline{74.32}$ ohms	$0.001332/\underline{90.87}$

Per mile values

	Resistance r_p, Ohms	Inductive Reactance jx_p, Ohms	Capacitive Susceptance jb_p, Micromhos
Measured	0.254 @ test temp.	$j0.823$	$j5.14$
Computed	0.238 @ 25 deg C	$j0.803$	$j5.18$

Characteristic impedance

	$\sqrt{Z_{ocp}\times Z_{scp}}$	$\sqrt{Z_p/Y_p}$	$\sqrt{z_p/y_p}$	$\sqrt{\frac{A_pB_p}{C_pD_p}}$
Measured	$409/\overline{8.77}$	$409/\overline{8.58}$	$409/\overline{8.58}$	$409/\overline{8.77}$
Computed	$403/\overline{8.28}$	$403/\overline{8.25}$	$403/\overline{8.25}$	$403/\overline{8.28}$

which were in service as part of the communication system used throughout tests, but 60-cycle effect of these was negligible. Atmospheric temperature during five or six hours of testing ranged from 75 to 90 degrees Fahrenheit with perhaps 85 degrees as an average value.

Phase rotation of terminal systems was also compared using a phase-sequence instrument and found to check transposition plan. This was later confirmed when systems were synchronized first time. Open- and short-circuit impedances, ***ABCD*** constants, unit impedance and admittance values, and characteristic impedances were calculated from instrument readings of amperes, volts, and watts as discussed in Appendix I.

Summary of Results

Tables I to VII, inclusive, summarize data taken during tests; compare line constants obtained from these data with those obtained from computations; and list other related information. For brevity, word "measured" applies to values obtained through supplementary calculations from test data, and word "computed" applies to values obtained from references or from computations based on physical dimensions of line and on conductor and shield wire configuration. In following discussions, "reactance" refers to inductive reactance, and "susceptance" refers to capacitive susceptance. Symbols are explained in Appendix VI.

Table I lists impedances as viewed from instruments with conductors at other end open- or short-circuited to ground and as obtained from supplementary calculations using data from ammeter, voltmeter, and wattmeter readings. Also included are computed values of open- and short-circuit impedances obtained from ***ABCD*** constants in Tables II, III, and IV. Tests *A* and *B*, *C* and *D*, *E* and *F* give basic data for positive-phase sequence, zero-phase sequence, and one-conductor-and-ground networks, respectively. Test *B* was repeated with conductors short-circuited together at far end but not grounded. Instrument readings were same as before. There was no potential between short-circuited conductors and ground, indicating balanced impedance of conductors.

Table II indicates that positive-phase-sequence values of resistance and reactance are higher than computed values with susceptance being slightly smaller. Difference between measured and computed values may be result of not taking into account effect of ground and shield wires in conventional computations and of angles in line and other factors causing departure from uniform construction. Characteristic impedance of 409 ohms is near 400-ohm value commonly accepted.

Table III shows that measured zero-phase-sequence resistance and reactance unit values are smaller than computed values which are based on small current flow of one or two amperes in shield wires believed to be comparable to that flowing during tests. During fault conditions, currents are high causing some change in resistance and reactance of shield wires. Assuming 40 amperes flowing in each shield wire, computed unit values are: $r_0=0.696$ and $x_0=2.44$. Susceptance values are same, possibly due to rather complete method of calculating referred to in Appendix V.

Table IV values for a one-conductor-and-ground circuit are of interest although they are not actually used in conventional high-voltage-transmission-line and system-fault calculations. Measured unit resistance is lower than computed. Reactance and susceptance values compare favorably. Computed values of resistance and reactance in this case, also depend on amount of current assumed flowing in shield wires. Of course, comparatively small currents flowed during measurements. Computed values listed assume about one or two amperes flowing. With 40 amperes assumed in each shield wire, computed unit resistance and reactance values are: $r_1=0.389$ and $x_1=1.35$. Characteristic impedance of 550 ohms measured at 60 cycles is of interest and compares with 500-ohm value commonly used for a single-wire and ground circuit and in design of lightning-proof lines. One-conductor-and-ground circuit measured forms part of carrier-current channel.

Table V summarizes characteristic impedances obtained from data taken during tests.

Table VI gives important ratios. Those obtained from measured data are lower except ratio of susceptances which are nearly equal.

Table VII gives ratios of sending-to-receiving current and voltages for the several tests. It will be noted that reciprocal of voltage rise ratios in tests *A*,

Table III. Zero-Phase-Sequence Constants

$A_0B_0C_0D_0$ Constants	A_0	B_0	C_0
Measured	0.735/6	545/75.1 ohms	0.000892/91.6
Computed	0.716/7.88	591/72.27 ohms	0.000885/92.17

Per mile values	Resistance r_0, Ohms	Inductive Reactance jx_0, Ohms	Capacitive Susceptance jb_0, Micromhos
Measured	0.636 @ test temp.	$j2.14$	$j3.65$
Computed	0.829 @ 25 deg C	$j2.28$	$j3.65$

Characteristic impedance	$\sqrt{Z_{oc0}\times Z_{sc0}}$	$\sqrt{Z_0/Y_0}$	$\sqrt{z_0/\ _0}$	$\sqrt{\frac{A_0B_0}{C_0D_0}}$
Measured	783/8.25	782/8.28	782/8.28	783/8.25
Computed	816/9.95	816/9.95	816/9.95	816/9.95

Table IV. Constants of One-Conductor-and-Ground Circuit

$A_1B_1C_1D_1$ Constants	A_1	B_1	C_1
Measured	0.798/4.39	340/73.79 ohms	0.001130/91.64
Computed	0.795/4.9	342/72.8 ohms	0.001145/91.43

Per mile values	Resistance r_1, Ohms	Inductive Reactance jx_1, Ohms	Capacitive Susceptance jb_1, Micromhos
Measured	0.408 @ test temp.	$j1.295$	$j4.46$
Computed	0.438 @ 25 deg C	$j1.3$	$j4.6$

Characteristic impedance	$\sqrt{Z_{oc1}\times Z_{sc1}}$	$\sqrt{Z_1/Y_1}$	$\sqrt{z_1/y_1}$	$\sqrt{\frac{A_1B_1}{C_1D_1}}$
Measured	550/8.93	551/8.75	551/8.75	550/8.93
Computed	548/9.32	547/9.32	547/9.32	548/9.32

C, and *E*, in which far-end conductors were not grounded, check very closely with the *A* constant as they theoretically should do for that particular arrangement of conductors. Reciprocal of current rise ratio of test *B* closely gives *A* constant. In this test conductors were tied together at far end and grounded, but as mentioned above, grounding had no effect on impedances measured. Also no current flowed through earth. However, reciprocal of current rise ratios in tests *D* and *F* do not give accurately the *A* constant. In tests *D* and *F*, conductors were grounded and current returned through ground and shield wires. This would indicate that unit values, particularly resistance and reactance, are slightly different for conditions with conductors grounded and not grounded in zero-phase sequence and one-conductor-and-ground circuit networks.

With no potential applied there was no indication of induced voltage on line when measured by potential transformers and voltmeters. During attenuation tests at carrier frequencies a two- or three-volt potential was observed between one conductor and ground using a vacuum-tube voltmeter shunted by 550-ohm resistor, indicating that any induced voltage effects were small. During test *E* in which 2,400 volts to ground was applied to one conductor only, potential between other two conductors and ground measured at far end was 300 volts. There was no appreciable unbalance of reactance between the three conductors as shown by three line-to-neutral impedance values of 257.5, 257.5, and 258 ohms which were computed using three-phase values taken during test *B*.

Table V. Summary of Characteristic or Surge Impedances

	Measured	Computed
From positive-phase sequence data. Conductor-to-conductor or conductor-to-equivalent neutral	409/8.77	403/8.28
From data taken with all three conductors tied together. Characteristic impedance between three conductors tied together and ground	261/8.25	272/9.95
Zero-phase-sequence characteristic impedance or three times preceding values	783/8.25	816/9.95
For one-conductor-and-ground circuit	550/8.93	548/9.32

Table VI. Ratios of Zero-Phase- to Positive-Phase-Sequence Resistance, Inductive Reactance, Impedance, and Capacitive Susceptance

	r_0/r_p	x_0/x_p	Z_0/Z_p	b_0/b_p
Measured	2.51	2.6	2.58	0.71
Computed	3.48	2.84	2.88	0.705

Table VII. Current and Voltage Rise or Increase

Test	Rise Current or Voltage at Energizing End Divided Into Values at Other End
A... Balanced three-phase potential applied to all three conductors with them open at other end	Voltage—1.18
B... Balanced three-phase potential applied to all three conductors with them short-circuited and grounded at other end	Current—1.2
C... Single-phase potential applied between all three conductors and ground with them tied together and ungrounded at other end	Voltage—1.36
D... Single-phase potential applied between all three conductors and ground with them tied together and grounded at other end	Current—1.4
E... Single-phase potential applied between one conductor and ground with it ungrounded at other end	Voltage—1.26
F... Single-phase potential applied between one conductor and ground with it grounded at other end	Current—1.34

Summary of Conclusions

Comparison of measured and computed constants for this type of line construction show:

1. Measured and computed positive sequence units of resistance, reactance, and susceptance are substantially equal. For most practical purposes conventional calculating methods of determining these constants are satisfactory.

2. Measured and computed zero-phase sequence impedances compare favorably. Computed values from line dimensions are satisfactory for most purposes.

3. Measured and computed constants compare favorably for a one-conductor-and-ground channel or circuit of type measured during tests.

4. Greatest difference between measured and computed line constants occurs in zero-phase-sequence resistance and reactance values. This is probably due to assumptions made and difficulty of taking into account all factors in complex physical arrangement of conductor and shield wires and connections of shield wires to ground; resistance and reactance of shield wires varies with current; position of shield wires above conductors varies along line; transpositions; and indefinite value of resistance of earth. Average earth resistivity may be less than assumed 100 ohms per meter cube as shown by values of resistance for zero-phase and one-

conductor-and-ground circuits being less than computed unit values.

5. Ratios of zero-phase-sequence resistance, reactance, and impedance to positive-phase-sequence values obtained from test measurements are less than those obtained from computed values. Ratio of susceptances as determined by measurement are slightly higher than those from computed values.

6. Measured characteristic impedance conductor-to-conductor of 409 ohms is higher than theoretical, but is near 400-ohm value commonly accepted.

7. Measured characteristic impedance of zero-sequence circuit is about 783 ohms and is lower than computed.

8. Measured and computed characteristic impedance conductor-to-ground with shield wires is about 550 ohms depending on current magnitude and ground conditions. One-conductor-and-ground circuit without shield wires may have characteristic impedance much greater than 550 ohms. Thus carrier relay tuning equipment connected between one conductor and ground should have adjustment range sufficient to permit matching impedances considerably above 400- and 500-ohm values commonly used for open-wire transmission circuits.

9. Zero-phase-sequence and one-conductor-and-ground circuit measurements were made with comparatively small currents flowing in shield wires, due principally to inherent low voltage of test method. Although resistance and reactance of shield wires vary with current magnitude and may be a different value during fault conditions when higher currents flow than when measured using low currents, values obtained by measurement check conventional methods of computing line constants.

10. Exact methods should be used in calculations to determine ground and phase currents for relay settings on long transmission lines. Short-line or approximate methods can result in errors of considerable magnitude.

Appendix I. Computation Procedure

Short- and open-circuit impedances viewed from instrument end and listed in Table I were determined from ammeter, voltmeter, and wattmeter readings. Pillar and architrave values of equivalent π network described in Appendix II were computed by means discussed in Appendix III. ***ABCD*** constants of network were determined from these by formulas given in Appendix IV, and were checked using rule $A \times D - B \times C = 1$ as well as other relationships listed in Appendix IV. Unit or per-mile values of resistance, reactance, and susceptance were determined from these ***ABCD*** constants by a cut-and-try procedure discussed in Appendix IV. A second group of ***ABCD*** constants was calculated from these unit values and compared with original ***ABCD*** constants as a check on the per mile values to see that they would yield constants equal to those from which they were derived. This procedure was followed for positive-phase, zero-phase, and one-conductor-and-ground network constants.

Unit positive-phase-sequence values were obtained from manufacturer of conductor and reference 4. Zero-phase-sequence and one-conductor-and-ground-circuit unit values were computed from line dimensions as explained in Appendix V. These unit values are compared with those obtained through measurement as listed in Tables II, III, and IV. ***ABCD*** constants were computed from these theoretical values. These constants also are compared with measured values in same tables, together with open- and short-circuit impedances and characteristic impedances. A and D constants were assumed equal.

All calculations were made on conventional 10-inch slide rules with most of them on vector types. No attempt was made to carry calculations and decimal places further than accuracy of basic test data. Considering computation procedure and that exact long-line formulas were used in determining constants and unit values, it is believed results are accurate within plus or minus one per cent.

The 60-cycle impedance of about 0.2 ohm of carrier-current traps was considered too small to be included in the calculations. Since sag and tension add only about one half of one per cent to actual length of conductors over surveyed line length, these factors were not taken into consideration.

While there may appear to be slight repetition of comparison of values, for instance characteristic impedances are given in Tables II, III, and IV and then they are summarized in Table V, this is done to acquaint reader with relative magnitude of these impedances and their relationship.

Positive-phase-sequence A and C constants in this paper differ slightly from those given previously[1] because of use of a more direct route of calculating them. That is, knowing B and Z_{oc}, D or A can be determined more directly from $Z_{sc} = B/D$, than from $Z'_{eq} = B/(A-1)$. However, these ***ABCD*** constants do not change unit values previously determined because those values are still within accuracy of method and basic data.

Appendix II. Equivalent Network

Figure 3 represents equivalent π network of transmission line used in calculating constants and checking impedance relationships of positive-phase-sequence, zero-phase-sequence, and one-conductor-and-ground circuit. For positive phase sequence, pillars represent shunt susceptance and architrave equivalent line or transfer impedance. Pillars represent line-to-ground capacitance and architrave the through impedance for all three conductors in parallel which was condition when basic zero-phase-sequence data were taken. For zero-phase-sequence circuit, these equivalent impedances are multiplied by three. For one-conductor-and-ground circuit, pillars represent capacitance between that conductor and ground, and architrave the through impedance.

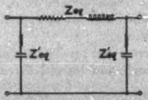

Figure 3. Equivalent π network used to represent positive-phase, zero-phase, and one-conductor-and-ground circuits

Referring to Figure 3, Z_{oc} is open-circuit impedance looking into network with opposite end open. Z_{sc} is impedance looking into network with opposite terminals short-circuited together.

Appendix III. Relation Between Z_{eq}, Z'_{eq}, Z_{oc}, and Z_{sc}

Refer to Figure 3:

$$Z_{sc} = \frac{Z_{eq} \times Z'_{eq}}{Z_{eq} + Z'_{eq}} \tag{1}$$

$$Z_{oc} = \frac{Z'_{eq}(Z'_{eq} + Z_{eq})}{2Z'_{eq} + Z_{eq}} \tag{2}$$

From (1):

$$Z_{eq} = \frac{Z_{sc} \times Z'_{eq}}{Z'_{eq} - Z_{sc}} \tag{3}$$

Substituting this in (2) and reducing gives:

$$Z_{oc} = \frac{Z'^{2}_{eq}}{2Z'_{eq} - Z_{sc}} \tag{4}$$

Solving for Z'_{eq}:

$$Z'_{eq} = Z_{oc}\left(\sqrt{1 - \frac{Z_{sc}}{Z_{oc}}} + 1\right) \tag{5}$$

which gives Z'_{eq} in terms of measured Z_{sc} and Z_{oc} values. Substituting results from equation 5 in equation 3 gives Z_{eq}. Equations 3 and 5 were used to obtain components of equivalent π from Z_{oc} and Z_{sc} values as measured.

Appendix IV. Relationships of General Constants and Unit Values

Following formulas were used to obtain ***ABCD*** values from open- and short-circuit impedances, and to check results:

$$Z_{oc} = \frac{A}{C} = \frac{D}{C} \tag{6}$$

$$Z_{sc} = \frac{B}{D} = \frac{B}{A} \tag{7}$$

$$Z_{eq} = B \tag{8}$$

$$Z'_{eq} = \frac{B}{A-1} \tag{9}$$

$$A \times D - B \times C = 1 \tag{10}$$

Expressions for ***ABCD*** constants from references 4 and 5 are:

$$A = D = \cosh\sqrt{ZY} = \left(1 + \frac{ZY}{2} + \frac{Z^2Y^2}{24} + \frac{Z^3Y^3}{720} + \ldots\right) \tag{11}$$

$$\boldsymbol{B}=\sqrt{\frac{\boldsymbol{Z}}{\boldsymbol{Y}}}\sinh\sqrt{\boldsymbol{ZY}}$$

$$=\boldsymbol{Z}\left(1+\frac{\boldsymbol{ZY}}{6}+\frac{\boldsymbol{Z}^2\boldsymbol{Y}^2}{120}+\frac{\boldsymbol{Z}^3\boldsymbol{Y}^3}{5{,}040}+\ldots\right) \qquad (12)$$

$$\boldsymbol{C}=\sqrt{\frac{\boldsymbol{Y}}{\boldsymbol{Z}}}\sinh\sqrt{\boldsymbol{ZY}}$$

$$=\boldsymbol{Y}\left(1+\frac{\boldsymbol{ZY}}{6}+\frac{\boldsymbol{Z}^2\boldsymbol{Y}^2}{120}+\frac{\boldsymbol{Z}^3\boldsymbol{Y}^3}{5{,}040}+\ldots\right) \qquad (13)$$

Unit resistance, reactance, and susceptance were obtained from equations 11 and 12 and from

$$\boldsymbol{Z}=L(r+jx) \qquad (14)$$

$$\boldsymbol{Y}=L(g+jb) \qquad (15)$$

by first equating value of $\boldsymbol{A}$ obtained from $\boldsymbol{Z}_{oc}$ and $\boldsymbol{Z}_{sc}$ to expression 11. $\boldsymbol{ZY}$ in series of (11) was found by cutting-and-trying. This was then substituted in (12), and knowing $\boldsymbol{B}$, $\boldsymbol{Z}$ was found and then $\boldsymbol{Y}$. Unit values were found from equations 14 and 15. $\boldsymbol{Y}$ was found to have a very small conductance component g, but this was not retained because its value was considered to be too indefinite for methods used.

Appendix V. Unit Resistance, Inductive Reactance, and Capacitive Susceptance From Line Dimensions

Positive-phase-sequence unit constants were obtained from tables and following methods given in chapter III of reference 4. Values obtained are:

$r_p=0.238$ ohm per mile at 25 degrees centigrade and 60 cycles
$jx_p=j0.803$ ohm per mile
$jb_p=j5.18$ micromhos per mile

These do not include effect of earth and shield wires.

Zero-phase-sequence unit constants were obtained following methods given in chapters VII, VIII, and IX of reference 5 and chapters XI and XII of reference 6. Values obtained are:

$r_0=0.829$ ohm per mile at 25 degrees centigrade and 60 cycles
$jx_0=j2.28$ ohms per mile
$jb_0=j3.65$ micromhos per mile

These include effect of shield wires. Average spacings and geometric-mean-distances were used in an effort to include effect of transpositions and different sag of conductors and shield wires.

Unit constants of one-conductor-and-ground circuit were obtained following methods given in chapters VII, VIII, and IX of reference 5 and chapters XI and XII of reference 6. Values obtained are:

$r_1=0.438$ ohm per mile at 25 degrees centigrade and 60 cycles
$jx_1=j1.3$ ohms per mile
$jb_1=j4.6$ micromhos per mile

These values include effect of shield wires. An average geometric-mean-distance between one conductor and shield wires was used in calculations for unit inductive reactance. Likewise average spacings were used in calculations for unit capacitive susceptance.

Following is a summary of basic assumptions, geometric-mean-radii, geometric-mean-distances, and related data used in computing unit constants from line dimensions:

An earth resistivity of 100 ohms per meter cube was assumed.
Equivalent depth of earth return was taken as 2,800 feet.

Conductor diameter = 0.683 inch.
Conductor resistance = 0.238 ohm per mile at 25 degrees centigrade.
Equivalent conductor spacing = 18.25 feet.
Geometric mean radius of conductor for inductive reactance = 0.0245 foot.
Geometric mean radius of conductor group for inductive reactance = 2.02 feet.
Geometric mean radius of conductor group with 14.5 feet spacing for capacitive susceptance = 2.11 feet.

Shield wire diameter taken as 0.375 inch.
Shield wire resistance:
  For current of 1 or 2 amperes = 4 ohms per mile.
  For 40 amperes = 7.4 ohms per mile.
Shield wire geometric mean radius for inductive reactance:
  Low current of 1 or 2 amperes = 10^{-3} feet.
  For 40 amperes = 10^{-10} feet.
Geometric mean radius of shield-wire group for inductive reactance:
  Low current of 1 or 2 amperes = 0.132 foot.
  For 40 amperes = 4.18×10^{-5} feet.

Geometric mean distance of separation between conductors and shield wires = 19.55 feet.
Geometric mean radius of shield wire group for capacitive susceptance = 0.523 foot.
Geometric mean distance of separation between conductors and images for capacitive susceptance = 67 feet.
Geometric mean distance of separation between one shield wire and conductor images for capacitive susceptance = 81.5 feet.
Average geometric mean distance of separation between one conductor and shield wires for inductive reactance = 19.5 feet.

See Figure 2 for average conductor and shield-wire spacings and distance above ground.

Appendix VI. Nomenclature

$\boldsymbol{ABCD}$—General constants of any four terminal network not having an internal source of voltage.
$\boldsymbol{A}_p\boldsymbol{B}_p\boldsymbol{C}_p\boldsymbol{D}_p$—Positive-phase sequence general constants.
$\boldsymbol{A}_0\boldsymbol{B}_0\boldsymbol{C}_0\boldsymbol{D}_0$—Zero-phase sequence general constants.
$\boldsymbol{A}_1\boldsymbol{B}_1\boldsymbol{C}_1\boldsymbol{D}_1$—General constants of one-conductor-and-ground circuit.
$r_pjx_pjb_p$—Positive-phase sequence per mile values of resistance and inductive reactance in ohms and capacitive susceptance in micromhos.
$r_0jx_0jb_0$—Zero-phase squence per mile values of resistance and inductive reactance in ohms and capacitive susceptance in micromhos.
$r_1jx_1jb_1$—One-conductor-and-ground per mile values of resistance and inductive reactance in ohms and capacitive susceptance in micromhos.
$\boldsymbol{Z}_{ocp}, \boldsymbol{Z}_{scp}$—Positive-phase-sequence impedance in ohms looking into line with far end open-circuited and short-circuited, respectively.
$\boldsymbol{Z}_{oc0}, \boldsymbol{Z}_{sc0}$—Zero-phase sequence impedance in ohms looking into line with far end open-circuited and short-circuited to ground respectively. These are three times impedances obtained with all three conductors paralleled.
$\boldsymbol{Z}_{oc1}, \boldsymbol{Z}_{sc1}$—Impedance looking into one-conductor-and-ground circuit with far end open-circuited and short-circuited to ground, respectively.
$\boldsymbol{Z}_{eq}$—$\boldsymbol{B}$ constant or architrave of equivalent general π network in ohms.
$\boldsymbol{Z}'_{eq}$—Impedance of shunt path in ohms at two ends or pillars of π network.
$\boldsymbol{Z}\underline{/\theta}$—Impedance in ohms.
$z\underline{/\theta}$—Impedance, ohms per mile.
$\boldsymbol{Z}_p\boldsymbol{Z}_0\boldsymbol{Z}_1$—Total line impedance or $L(r_p+jx_p)$, $L(r_0+jx_0)$, $L(r_1+jx_1)$ respectively.
$\boldsymbol{Y}_p\boldsymbol{Y}_0\boldsymbol{Y}_1$—Total line admittance or $L(g_p+jb_p)$, $L(g_0+jb_0)$, $L(g_1+jb_1)$ respectively.
L—Line length in miles.

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A Pneumatic Mechanism for Outdoor Oil Circuit Breakers

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OPERATING mechanisms for power circuit breakers differ from most machines in that:

1. Moderate amounts of energy must be released quickly and easily at a high power rate.
2. Energy must be available for their operation under emergency conditions, particularly when normal power sources would be interrupted.

These particular conditions are satisfied readily by a stored-energy system where energy is accumulated with moderate rates of power and held ready for instant consumption. Stored-energy systems can be selected in many forms. The rubber bands of a toy airplane are one form, the rocket projectal another. Switchgear however places a premium on reliability, not on lightness or novelty, a demand that considerably restricts the practical choices that can be made.

Of the several choices that are available, storage batteries, compressed springs, and compressed-air systems have histories of satisfactory applications as energy accumulators for power circuit breakers. The high closing speeds of modern oil circuit breakers demand a rather large amount of power during a short interval of time. The larger circuit breakers may require more than 5,000 foot-pounds in 20 cycles during closing. To close a breaker, the mechanism must transform 20 kw of power to mechanical energy. Such demands for power can be supplied most efficiently from an energy accumulator located close to the circuit breaker. Losses over any distance naturally will reduce the available power at the breaker unless the capacity of the transmission channel, such as control-power leads or air lines, is increased considerably. Storage batteries suffer that particular handicap. They are economical when located centrally, and where they can be properly maintained and protected. They are not economical nor very practical when placed adjacent to each of a scattered group of circuit breakers. With batteries located at a central point, the *IR* drop of the control leads is of concern when control power is distributed over the distances to the breakers of a high-voltage yard.

Energy, however, can be economically and practically stored at each circuit breaker as compressed air or as stressed springs. Air can be compressed by small commercial motor-driven air compressors, stored in a moderate-size tank and released by an electrically operated control valve. Energy also can be accumulated and stored by compressing springs through a motor and gear-reduction unit, released and controlled by a suitable system of levers and latches. The methods of accumulating, storing, releasing, and controlling the energy of these media determine the success of the final circuit breaker under operating conditions. The mechanism discussed in this paper uses compressed air as the stored-energy operator. The choice has been made after a careful analysis of the listed advantages:

1. Energy is accumulated through a simple and familiar fractional-horsepower motor and standard air compressor. Power demands are held to a low value.
2. The energy is stored in an air-storage tank at the circuit breaker.
3. The closing power is released and controlled through a low-energy electrically operated air valve, other mechanical reductions being unnecessary.
4. Compressed air has little inertia. It is fast, and its resilience moderates accelerating forces.
5. The speed of response and lack of inertia of compressed air permit a ready reversal of flow to trip free the contacts of a circuit breaker.

Against these choices must be considered the apparent handicaps of compressed-air systems:

1. Storage must be nearly airtight, and, theoretically, leakage must never equal the rate of accumulation by the compressor. Practically, the leakage should be held to a small percentage of this rate.
2. Moisture in the air may give difficulties at freezing temperatures.

The mechanism here described has been developed with emphasis on the elimination or control of the handicaps listed. Practical solutions to the problems of moisture formation with freezing and air leakage have been developed and tested carefully. It is believed that this compressed-air system offers the advantages of a stored-energy system not readily available in other conventional operating mechanisms. See Figure 1.

Operating Characteristics

The value of a circuit-breaker operating mechanism cannot be determined on the basis of energy storage alone, for the choice of an operating medium must include a study of operating characteristics under "closing," "tripping," "tripped free," and "immediate reclosing" performance cycles. This comparison of compressed air with other media already has been presented on the basis of energy storage. The benefits of pneumatic systems when designed to satisfy these other operating characteristics will be discussed.

CLOSING CHARACTERISTICS

With ample energy stored at the breaker as compressed air, the breaker contacts can be closed with desirable velocity and force characteristics through an air cylinder. Air compressed above 100 pounds per square inch responds with unusual speed to control demands. Opening the air-control valve admits air to the cylinder assembly at a rate that permits full pressure build up before the contacts are in motion. Pressure is maintained at a rate determined by a controlled orifice, accelerating forces, and the opposing forces of the contacts. Figure 2A illustrates a typical pressure and travel curve as this mechanism closes a standard oil circuit breaker. Contact speeds may be controlled closely by selection of the operating pressure. A controlled flow of air limits the "runaway" and "two-stage" action on contacts characteristic of some prime movers. This two-stage action, or hesitancy when the contacts engage, is particularly undesirable when the breaker is closing against loaded lines or against fault current. Compressed air is positive and powerful in closing a circuit breaker.

TRIPPING CHARACTERISTICS

When used with a modern tank-type oil circuit breaker, a pneumatic mechanism uses compressed air to complete only the closing operation. Tripping or opening the breaker is accomplished by simply disengaging a holding latch or prop. The release of this trip latch frees the opening and contact springs to open the contacts. The inertia of most prime movers may retard this opening speed unless the prime

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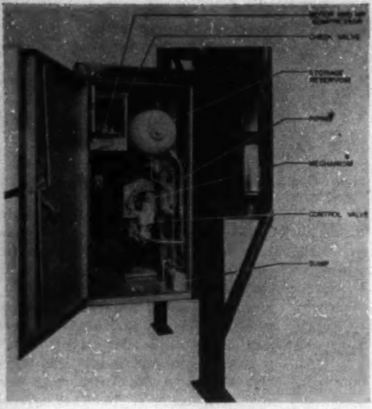


Figure 1. A pneumatic operating mechanism for use with frame-mounted outdoor oil circuit breakers, viewed through the right-hand doorway

mover is divorced from the contact structure by a collapsible trip linkage. Such a linkage may delay a reclosure while it resets. The compressed-air mechanism, with little inertia, does not require a collapsible trip linkage. The output crank, freed from the holding latch by the trip coil, swings to its open position, and the mechanism is prepared to reclose the circuit breaker without delay.

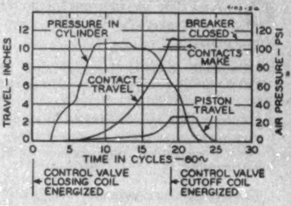
TRIP-FREE CHARACTERISTICS

In trip-free operations it is desirable that the contacts of the breaker be reversed in their motion before a closing operation is completed. Heavy solenoids or motors, driving a closing mechanism, would be too slow in their mechanical response to a demand for reversal of motion were it not for the mechanically trip-free mechanisms employed. Such designs use collapsible linkages held rigid by a latch. Placed between the prime mover and the contact structure, this linkage can be collapsed by releasing the latch with a solenoid trip coil. The contacts of the circuit breaker, deprived of their closing force, quickly return to the open position. Unfortunately, the prime mover, being slow in mechanical response, commonly continues to drive onward, acceleration and impact forces replacing the suddenly lost contact-closing load.

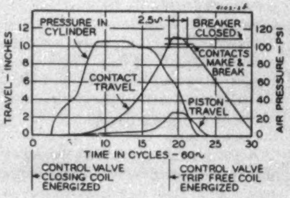
The speed of compressed air and its immediate response to control provides a practical prime mover requiring only speedy control to reverse or release its driving power within the time limits required of a trip-free operation. Fast control is provided by an electrically operated air-control valve. Power release is controlled by a dump or release valve placed in the air line between the control valve and the cylinder. This dump valve is closed or opened by the balance of pressure between the control valve and cylinder. It is closed when the control valve is open, the piston then closing the circuit breaker. If, during the closing cycle the control valve is closed, the sudden loss of pressure on the control valve side of the release or dump valve moves it to the open position, opening the cylinder port and exhausting the air in the main cylinder. With this sudden loss of pressure, and closing force, the circuit-breaker contacts return to their open position (Figure 2B). Thus a trip-free operation is affected by closing the solenoid control valve during the closing stroke of the circuit breaker. Actual operating speeds equal or may exceed those of mechanically trip-free mechanisms using collapsible linkages. Accelerating forces are not sacrificed to moving such links and levers. Excess energy is released as a blast of air from the release valve. An original design of the solenoid control valve provides the speed desirable in the electrical and pneumatic control (see Figure 4).

IMMEDIATE-RECLOSING CHARACTERISTICS

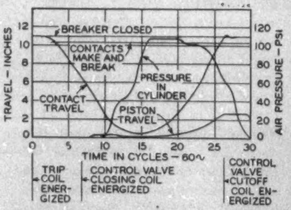
By reclosing instantaneously against a temporarily faulted line, modern oil circuit breakers have, in many applications, made a major contribution to service continuity. In many cases the worth of this contribution is dependent on the speed of reclosure. From the closed and latched position, the system may demand that the circuit be cleared and re-energized in as short a time as 20 cycles. Added to this requirement is the demand that the mechanism be prepared to trip free during the second reclosure. Mechanically trip-free mechanisms are available to satisfy these requirements. Their arrangements are generally particularly designed for such service. On the other hand the standard pneumatically trip-free mechanisms serve this purpose. The pneumatically trip-free mechanism is prepared to open, reclose, trip free, and reclose the contacts of a circuit breaker without delay in succes-



A. A closing operation with 120 pounds per square inch in the storage reservoir



B. A trip-free closing operation at 120 pounds per square inch, with the trip circuit energized through the main contacts for this no-load test. The short time in contact (2.5 cycles) demonstrates the speedy response of this pneumatic operator



C. An instantaneous-reclosing operation at 120 pounds per square inch. Attention is directed to the initial travel of the piston at low pressure. Here it meets the backward movement of the contacts, gradually stopping their motion and returning them to the closed position

Figure 2. Time-travel curves of a pneumatically operated standard outdoor oil circuit breaker

Instantaneous relations of cylinder pressure, piston travel, and breaker-contact travel are recorded. The curves are to a common scale

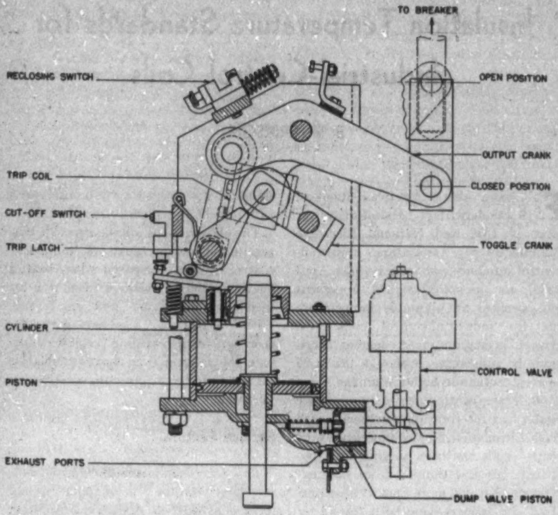

TO BREAKER
RECLOSING SWITCH
OPEN POSITION
OUTPUT CRANK
TRIP COIL
CLOSED POSITION
CUT-OFF SWITCH
TRIP LATCH
TOGGLE CRANK
CYLINDER
CONTROL VALVE
PISTON
EXHAUST PORTS
DUMP VALVE PISTON

Figure 3. A diagram of a pneumatically trip-free operating mechanism latched in the closed position

The control valve has closed; pressure in the cylinder has moved the dump-valve piston to the right, uncovering the ports and exhausting the cylinder

sive operations (Figure 2C). Proper attention to control circuits makes this a most satisfactory operating mechanism for instantaneous-reclosing applications.

Description of the Mechanism

Functionally the operating mechanism may be divided into:

(*a*). The prime mover, mechanical linkage, and operating controls.

(*b*). Compressor, air-storage equipment, and controls.

The Prime Mover, Mechanical Linkage, and Controls

The arrangement of the mechanism is illustrated in Figure 1. It includes a frame, a closing linkage, a trip latch, a cylinder and piston assembly, a dump valve, and a control valve. The mechanism is supported rigidly on a pair of vertical channels forming part of the housing and bolted directly to the supporting framework of the oil circuit breaker.

The closing linkage, Figure 3, consists of the output and toggle cranks arranged to operate as rolling toggles during the closing and in the latched position. As a toggle this system adjusts the closing forces of the piston to the contacting forces of the circuit breaker. In the closed position a trip latch or prop drops under a roller on the lower toggle crank. Until this latch is forced from under this roller either by the trip solenoid or by hand, the breaker contacts are locked closed. An air-operated cylinder and piston are mounted directly to the mechanism frame. The piston plunger operates against the roller of the lower toggle crank to force the mechanism to the closed and latched position. A hydraulic jack may be used against the lower piston extension for manual operations when the breaker is being inspected or maintained. Because air pressure in the cylinder is transient, it is not necessary to seal this part of the system against normal leakage. The piston is fitted with a conventional piston ring. Mechanical clearances are not unduly close.

The operation of the air piston is controlled by an electrically operated air valve shown in Figure 4. Energizing the "operating coil" at the top of the valve lifts an armature coupled to a pilot valve. As this valve is opened, an unbalance of pressures opens the main piston valve. In the open position the armature seals against a small permanent-magnet pole piece. Near the end of the power piston's closing stroke an auxiliary switch energizes a small cutoff coil adjacent to the magnetic pole piece. The flux of the permanent magnet is shunted out by the field of this coil and releases the armature. The pilot valve and, in turn, the main control valve close. With the control valve

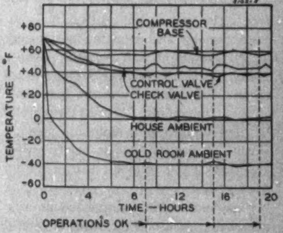


Figure 5. Records of a representative test of a pneumatic mechanism under controlled cold-room conditions

Air was accumulated in the storage reservoir while the room was at 70 degrees Fahrenheit with a relative humidity of 96 per cent. The ambient temperature of the room was then dropped to and held at 40 degrees Fahrenheit below zero. The compressor, pressure switches, control, dump, and check valves were operated at indicated intervals. Recorded temperatures are shown during this test run

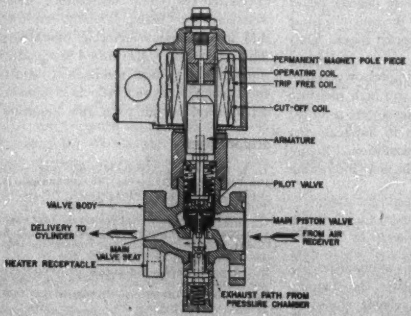

PERMANENT MAGNET POLE PIECE
OPERATING COIL
TRIP FREE COIL
CUT-OFF COIL
ARMATURE
PILOT VALVE
VALVE BODY
MAIN PISTON VALVE
DELIVERY TO CYLINDER
FROM AIR RECEIVER
MAIN VALVE SEAT
HEATER RECEPTACLE
EXHAUST PATH FROM PRESSURE CHAMBER

Figure 4. An electrically trip-free air-control valve

The valve is sealed open magnetically and recloses as the holding flux is shifted by energizing either the trip-free coil or the cutoff coil closed, the dump valve slides to its open position, and the compressed air in the operating cylinder is released.

For trip-free operations a small trip-free coil similar to the cutoff coil is energized in parallel with the trip coil, the control valve is closed, and again the compressed air is released from the main cylinder. This trip-coil circuit can be energized from a conventional d-c source. The trip-coil currents are comparable to those of other mechanisms, being six amperes at 125 volts direct current.

COMPRESSOR, AIR-STORAGE, AND CONTROL EQUIPMENT

The problems peculiar to compressed-air mechanisms for circuit breakers include reliable air storage and freedom from air stoppages due to the freezing of accumulated moisture. As an answer to these problems, every effort was made to simplify all piping and to have all such piping drain to a common sump. A minimum of joints reduces the possibility of leaks. Proper drainage reduces the difficulties with accumulated moisture.

A standard-type air compressor, driven by a fractional-horsepower motor, is mounted adjacent to the storage reservoir near the top of the mechanism housing. A coil of formed tubing serves as an aftercooler between the compressor and the storage tank. The tank, manufactured to all code requirements, has storage capacity for at least five successive breaker operations. Condensate is accumulated in a small sump at the lowest point in the house. A hand valve is used to blow off this sump.

Pressure switches control the air system. The first switch controls the compressor motor and regulates the pressure at the reservoir. A second pressure switch opens the breaker-closing-control circuit if the storage pressure is below a working minimum. A third switch can be used to operate a minimum pressure alarm. A pressure gauge, safety valve, and check valve complete the air-system accessories.

Experience with compressed-air systems has emphasized the value of careful design and design testing to prevent possible difficulties from freezing. Any troubles at freezing temperatures originate with moisture accumulations. The air lines of the operating mechanism are laid out to reduce moisture pockets to a minimum.

As a guard against freezing during subzero temperatures, small resistance heaters are installed at the control valve and on the supporting base of the compressor. A small space heater, located below the mechanism, moderates the temperature of the house. These heaters are controlled by a thermostat which energizes these heaters when the inside ambient temperature is less than 50 degrees Fahrenheit.

With these provisions, operations have proved satisfactory to 40 degrees below zero Fahrenheit, under the controlled-test conditions of a refrigerated room. During an extended series of tests in this refrigerated room the compressor accumulated air of both low and high moisture content. Continuous tests were recorded over extended periods to duplicate the more severe operating conditions. Figure 5 summarizes some of the more interesting records.

Summary

This compressed-air mechanism has been developed to answer the requirements of a simple and reliable stored-energy mechanism. By arranging this mechanism to be pneumatically trip-free, it has been possible to get the high-speed operations required of modern oil circuit breakers. The mechanism has been tested under severe operating conditions to insure trouble-free performance. Time-travel curves show satisfactory characteristics for closing, tripping, trip-free, and instantaneous reclosing operations. The absence of synchronized mechanical motions and critical mechanical adjustments contributes to a reduction in maintenance and will add to reliable operation.

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Insulation Temperature Standards for Industrial-Control Coils

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FOR many years, American Standard *C*19 (Underwriters Laboratories, Inc. issue of 1938 and National Electrical Manufacturers' Association industrial-control standards publication 40-59, April 1940) for industrial-control apparatus has specified an allowable temperature rise for industrial-control coils with class-*A* insulation of 85 degrees centigrade by resistance. Although this is 25 degrees centigrade higher than the value of 60 degrees centigrade rise by resistance established for rotating machinery with class-*A* insulation, service experience with control coils has been thoroughly satisfactory. It is estimated from field experience that not more than five per cent of all coil failures have been caused by temperature, despite the frequent use of above-normal voltages. The author believes, therefore, that the present control standard has proved to be conservative with respect to temperature-rise limits.

The chief reasons for the superior temperature–life expectancy of industrial-control coils as compared with rotating machinery having class-*A* insulation are their rigid structure and small size. These conditions together with the thorough varnish sealing possible, eliminate the failures from brittle insulation that occur in large apparatus subjected to vibration.

Service Factors

This fact that insulation life depends very largely on the type of apparatus and service, as well as upon the insulating materials used, was recognized in the 1940 revision of AIEE Standard 1, as indicated by the following quotations from its preface:

> ". . . How long an insulation will last electrically will depend not only on the class of material used, but also on the effectiveness of the physical support for the insulation, and the severity of the physical forces tending to disrupt it. Even though portions of insulation structures of organic materials

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> have become oxidized or embrittled under the influence of high temperatures, successful operation of the windings may be obtained over further periods of years, if such insulation is not mechanically disturbed.
>
> "In view of the important influence of mechanical stresses, thermal expansion and contraction forces impose temperature-rise limitations on larger apparatus, even though higher temperature rises have proven to be satisfactory in smaller machines.
>
> "Insulation life is also dependent to a considerable extent upon the access of oxygen, moisture, dirt, or chemicals to the interior of the insulation structure. At a given temperature, therefore, well-impregnated windings or totally enclosed machines may have much longer insulation life than windings freely exposed to industrial atmospheres. . . .
>
> ". . . It is important that individual types of apparatus and specific-purpose applications should be free to use different temperature-rise values in accordance with their individual economic problems."

At any rate, hundreds of thousands of class-*A*-insulated industrial-control coils, designed for a rise by resistance of 85 degrees centigrade at 100 per cent voltage, have been in use in industrial applications for the past 25 years with entire success and customer satisfaction of a high order. This experience, therefore, supports the author's conviction that the temperature-rise limits should be extended:

(*a*). To take advantage of advances made in the art of coil design and manufacture.

(*b*). To utilize to the fullest possible degree opportunities offered by the many new synthetic materials which have come on the market in recent years.

Better Designs and Service

In reference to paragraph *a*, such improvements as have been made in molded compounds, varnishes, cloths, paper, and wire should be and have been reflected in better and longer-lasting coils. Designs today tend to be even smaller and more compact and for this reason do not readily deteriorate mechanically subsequent to overheating (for typical appearance of control-coil designs see Figure 1). Improvements in coil construction will result in smaller and lighter designs. These when used in contactors and relays speed up device operation, prolong life of moving parts, tend to reduce maintenance, and, in general, give greater customer satisfaction.

Also much has been learned about methods of support so that present coil designs are well and firmly held in place. This means that the drying out of the insulation at elevated temperatures will not result in mechanical failure of the insulation system. Another factor which is of considerable help is the use of class-*A* non-water-absorbent insulation materials. Old-time papers and cloths when dried out and later subjected to moisture conditions act as a wick absorbing considerable quantities of water. This action, of course, is productive of current leakage and electrolytic corrosion of the copper wires and is far more destructive to coil life than temperature aging by itself.

This temperature-aging moisture-absorption characteristic does not apply in the same degree to modern synthetic insulation materials.[1] Cellulose acetate, polystyrene, polyvinyl-chlorides, polyamides, and the glass or glass-laminated products are far better than the papers, silks, and cottons in common use when the present standards were first written. This is substantiated by field experience which has shown that temperature failures are less than five per cent of total coil failures.

New Materials

Again if we consider paragraph *b*, these new materials[2] and their applications do not fall too well within our previous concept of class-*A* materials. Nylon, for instance, is a good illustration of a material of organic origin that will withstand temperatures in excess of those permitted by AIEE class *A*. On the other hand it cannot be classified as class *B*. There are many materials of this general nature that will be available after the war.

For instance, as an illustration of the many new synthetic materials which favor a change to higher temperature standards, we have such materials as General Electric Company Permafil, Westinghouse Electric and Manufacturing Company Fosterite, Bakelite Corporation number 16,631, Columbia Corporation resin *CR* 39, and Monsanto Chemical Company number 100 type of resin, and a host of others, manufactured by such companies as Klascon, Ault and Wiborg, Marco, and others. These materials are chemically different from old-style conventional oil varnishes and beyond a doubt, far superior at elevated temperatures when properly applied.

Test Data

Actual long-time test data running into years of field experience are not available for most of these materials, operating at their top temperature limits. However, much practical field experience has been gained over the last two-year period in applications where these materials have been in demand. Such applications as common to the aviation industry are very nicely taken care of by the high power factor, high dielectric and temperature-endurance characteristics of these synthetics.

Figure 1. Typical industrial-control contactor coil

A few selected cases of test data on coils made with General Electric Company Permafil are:

A. A group of coils wound with 1,100 turns of 0.0179 wire were operated 600 hours at 185 degrees centigrade total. Slight cracking of the insulation appeared at one end of one coil, but the others remained in perfect operating condition throughout the test.

B. Another group of coils being slightly larger, approximately four inches square, and consisting of 2,000 turns 0.0126 wire have to date been operated for 850 hours at 165 degrees centigrade total temperature. To date there is no indication whatever of insulation deterioration.

C. Yet another group of coils of the paper-layer-wound variety insulated with Permafil and consisting of 6,000 turns of 0.010 wire have operated 1,000 hours at 150 degrees, centigrade without any signs of cracking, crazing, shrinkage, or other deterioration.

D. A filament transformer has been operated on test 1,500 hours, with a voltage between terminals of 25 kv at a total temperature of 145 degrees centigrade. This coil is still in perfect condition.

Laboratory Tests

Much work has been done by the various laboratories interested in improving insulation materials. This, of course, applies to nearly all leading electrical manufacturers. Tests made by the author's associates include heat aging on test specimens at 150 degrees centigrade for one-year period and comparison of power factor and dielectric after this period of time. Among the materials tested, Permafil showed no deterioration during these tests. This applies as well to the electrical characteristics as to such mechanical considerations as shrinkage cracking, discoloration, or embrittlement.

From the multitude of tests mentioned, the conclusion is evident that the heat-resistant synthetics of today are much superior to the common class-*A* materials. What shall we do with these new materials? The author believes that now is the time to appraise these newcomers to the insulation field and where available introduce them on current designs. However, to be able to do this, we must change the rules under which we operate.

New Heat-Resisting Synthetic Insulating Materials

It is recognized that all synthetics are not improvements over natural organic insulating materials. For this reason it is very difficult to classify the heat-resistant synthetics or to group them under family names; most of them do not have readily understandable names and are known only as numbers. It is proposed, therefore, that these materials be grouped under the broad title of "Heat-Resisting Synthetic Insulating Materials." The acceptance of any particular material under this definition ultimately should be dependent on proof that it meets the requirements established by a (future) test code of insulating materials, or by accelerated life tests on complete coils.

Application Problems

The existing temperature standards referred to in the introduction are based on continuous operation. This, of course, does not apply to the vast majority of control-coil uses, and for that reason recognition should be given this fact as part of the standards.

Actually, very few control coils or motors operate under continuous-service conditions. This is a point that sometimes is overlooked in considering insulating materials. Much has been written about life expectancy of class-*A* materials.[3] The literature indicates that the useful life of organic insulations such as cotton, oil, and paper, is of the order of 7 to 15 years when exposed to 105 degrees centigrade continuously, with free access of air and moisture, and that the life is halved by each 8 to 12 degrees centigrade increase in temperature. Of course, the actual life in a particular case is increased if oxygen and moisture are excluded,[4] and if exposure to maximum temperature occurs only part of the time. These latter factors account for the fact that control-coil life under present temperature-rise standards has approximated ten years, in the author's experience.

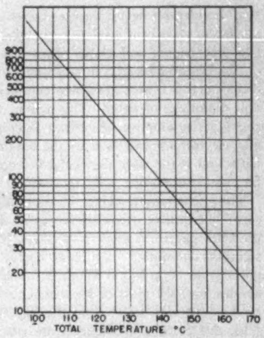

Figure 2. Effect of temperature on life of class-*A* insulated magnet coils

Taking everything into account, the author believes that an 11 degrees centigrade temperature increase for half life is the most reasonable assumption for organic insulation in agreement with reference 3 and as indicated in Figure 2. This curve is drawn with the 100 per cent life point located at 140 degrees centigrade, corresponding to 100 degrees centigrade rise over a 40 degrees centigrade ambient temperature. This curve is useful in predicting life expectancy at higher or lower temperatures of otherwise identical applications.[5]

It is expected that the new temperature-resisting synthetic materials will have an even better service life at this 140 degrees centigrade total temperature value than has been the case with the older class-*A* insulation at the present limiting temperature of 85 degrees centigrade rise plus 40 degrees centigrade ambient temperature, or 125 degrees centigrade total.

It is of extreme importance to judge such data correctly in terms of probable life under actual operating conditions. Duty cycle, ambient temperatures, time for coils to attain maximum temperature, enclosures, and all other special conditions must be considered. It is and should be the application engineers' problem to evaluate such data. This cannot be done by blind adherence to present AIEE Standards.

For instance, in the case of aircraft control coils, these are made to give satisfactory service at temperatures well above the 85 degrees centigrade rise plus 40 degrees centigrade ambient temperature. Considering expected life, this is sound application engineering and the AIEE rules should be made flexible enough to include such cases.

Hot-Spot Temperatures

With today's design practices, the hot-spot differential for industrial-control coils is much lower than in past practice and is normally below the five degrees centigrade contained in AIEE Standard 1.

The elimination of much fabric coil and layer insulation by the substitution of new extra-tough enamels and other dense materials has reduced the temperature gradient to practically zero.

Recommendations

In view of the preceding considerations, it is recommended that the following changes be made in the present AIEE Standard 1 applying to industrial-control coil designs:

1. For industrial-control coils of 600 volts rating or less, using chiefly heat-resisting insulating materials, not dependent for their insulating strength on any natural organic materials such as cotton, paper, or varnished cloth, and thoroughly impregnated with high-temperature synthetic varnishes, the permissible temperature rise by resistance should be 100 degrees centigrade.
2. For industrial-control coils of 600 volts rating or less, with insulation composed of a high percentage of inorganic materials, such as glass or mica, and wound on assemblies of heat-resisting synthetic insulating materials, the permissible temperature rise by resistance should be 120 degrees centigrade.

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Gas-Filled-Cable Research and Experience

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MEMBER AIEE

RESEARCH and engineering studies of gas-filled cable were started about 12 years ago by the company with which the writer is associated and this development work has been carried on continuously since then. Useful results in the form of practical cable designs have resulted and more will follow in the future as our knowledge of the basic principles of this type of cable increases. The present paper will be in the form of a progress report summarizing results obtained since the first two papers[1,2] on this subject were presented before the AIEE in 1939 and 1942. The same methods of test and analysis of test data described in the previous papers will be followed and, although an attempt will be made to have the present paper complete in itself, it will be necessary to refer to the older papers frequently to avoid repetition.

The soundness of gas-filled-cable design is now well proved by more than five years of practical operating experience and this type of cable can no longer be considered experimental. There have been no service failures or signs of insulation deterioration and during this period a total of something like 20 separate installations involving low-, medium-, and high-pressure types, at voltage ratings from 15 to 120 kv, have been produced in this country by cable manufacturers. Most of this experience has been with the low-pressure type at voltage ratings up to 38 kv. There has, however, been limited experience with the medium- and high-pressure types. Uniformity and control of dielectric characteristics through gas-pressure control, simplicity of design and operation, economy, self-supervision of mechanical defects, and other features assure future usefulness.

Ionization as a Function of Gas Pressure

The establishment of a reliable curve showing initial ionization starting voltage as a function of gas pressure furnishes a useful and important guide for design and operation of gas-filled cable. A curve of this kind was given in the last paper[2] and its significance was discussed at some length. Due to the ionization self-extinguishing effect in mass-impregnated gas-filled cable, which will later be dealt with in detail, the initial ionization starting voltage represents an operating voltage limit that can be closely approached with safety. In this respect, gas-filled cable is the only known type that offers a direct way of predetermining safe operating voltage stress and insulation thickness by means of a simple, short laboratory test on advance samples. It is for these reasons that close attention has been paid to the establishment of a reliable average curve of this type.

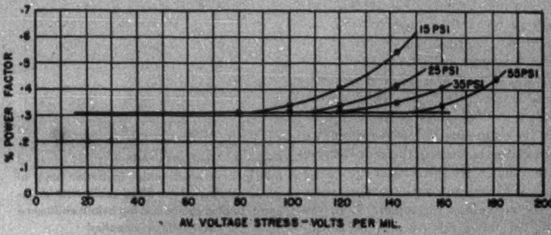
The curve in the earlier paper[2] was known to be accurate up to a gas pressure of 30 pounds per square inch, since it was based on a number of separate tests up to that pressure. Additional tests made during the past two years have confirmed this. There were reasons, however, to doubt the old curve at pressures above 30 pounds per square inch. For one thing, the shape of the curve at the higher pressures did not agree with theoretical calculations, having too much droop. The peculiar shape of the power factor curves in Figures 3 and 4 of the first paper[1] also indicated the possibility of extraneous effects. Later, it was confirmed that the gas-filled test terminals were faulty and terminal discharges accounted for these discrepancies. Additional high-pressure measurements have since been made under better controlled conditions.

Figure 1 gives power-factor curves at pressures from 15 to 55 pounds per square inch on a commercial length of 46-kv three-conductor gas-filled cable with double-reinforced sheath. The cable was impregnated with standard number 5317 compound and had received the usual standard treatment and compound drainage before test. It will be noted that ionization voltage up to 30 pounds per square inch agrees with that measured on standard low-pressure gas-filled cable as reported in the past[1,2] the ionization voltage stress being 85 volts per mil at 15 pounds per square inch.

Figure 2 shows corresponding power-factor curves at pressures from 10 to 225 pounds per square inch on a special length of hollow-core single-conductor high-pressure cable in a steel pipe. It

Figure 1. Three-conductor medium-pressure gas-filled cable, 400,000 circular mils, compact round conductors, strand shielding, 0.280-inch insulation, rated 46 kv to operate at 40-pounds-per-square-inch nitrogen pressure, showing initial ionization starting voltage at different pressures after drainage of surplus compound before load-cycle aging test

Measurements at room temperature



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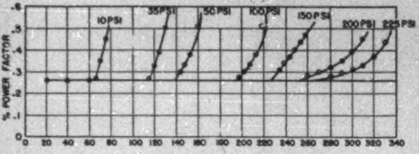

Figure 2. **Single-conductor high-pressure gas-filled cable—350,000-circular-mil conductor with one-half-inch hollow core, strand shielding, 0.300-inch insulation, lead sheath, cable in steel pipe, showing initial ionization starting voltage at different pressures after very severe drainage of insulation at 100 degrees centigrade**

Measurements made at room temperature

was also impregnated with standard number 5317 compound in the usual way but the compound drainage treatment was much more severe than standard, the purpose being to remove all possible surplus compound from the insulation and thereby obtain more sharply defined curves. It is always difficult to obtain sharply defined ionization starting voltage with standard cable, especially at the higher pressures. Obviously, also, such results obtained on severely drained cable are on the safe side when applied to standard cable.

In this case, drainage treatment before leading consisted of application of ten pounds per square inch nitrogen pressure to the conductor core, with the reel length under vacuum in an impregnating tank at 100 degrees centigrade. This forced drainage continued for two days and examination of end samples showed the whole cable cross section to be quite "dry," with little evidence of surplus compound between tapes. This "dry" condition is further verified by the sharply defined ionization curves in Figure 2, and the relatively low ionization starting point of 65 volts per mil at ten pounds per square inch. Standard cable averages 75 volts per mil at this pressure.

The results in Figures 1 and 2 were replotted in Figure 3 and a revised curve of initial ionization starting voltage as a function of gas pressure constructed as shown. Up to 30 pounds per square inch this curve is the same as the old one.[1] Above 30 pounds per square inch the new curve has a much more rational shape and agrees better with the theoretically calculated curve. That there is not closer agreement might be due to any one of several things. Theoretical calculations of this kind are, at best, approximate. Extraneous results at the higher pressures and stresses might possibly still be present in the test measurements, even though of lesser degree. Finally, the "dry" condition of the insulation undoubtedly lowered the measured curve below the theoretical curve at the higher pressures, since the latter was based on results with standard cable up to 30 pounds per square inch, the extension above this pressure being calculated from Paschen's law,[3] corrected for measured dielectric strength of nitrogen gas at the higher pressures.[4]

Working Voltage Stresses

The disagreement between calculated and measured results is not important, since even the measured curve in Figure 3 shows ionization voltage stresses at the higher pressures to be well above any working stresses that might be used in actual designs. For purposes of co-ordinated impulse strength the insulation thickness for a 138-kv high-pressure cable would correspond to an average 60-cycle working voltage stress of not more than 145 volts per mil. Figure 3 shows that even with a gas pressure as low as 80 pounds per square inch this working voltage stress is well below ionization voltage stress. At present, reinforced-sheath medium-pressure gas-filled cable operates at a pressure of 40 pounds per square inch and an average working voltage stress of 100 volts per mil. Reference to Figure 3 will show this to be well below the ionization-voltage curve. To a lesser degree, the same thing can be said of single-sheath low-pressure cable operating at 12 pounds per square inch and 65 volts per mil. In all three types there is room for further reduction in insulation thicknesses in the future, as far as 60-cycle voltage stresses are concerned. In this respect, no attempt has yet been made to take full advantage of the ionization self-extinguising characteristics of mass-impregnated gas-filled cable.

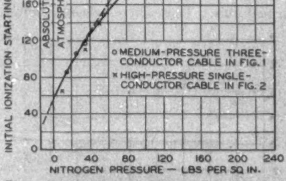

Figure 3. **Ionization voltage stress on solid insulation at which initial ionization starts in mass-impregnated gas-filled cable after drainage of surplus compound but before endurance test voltage is applied**

Automatic Grading and Self-Healing

Some engineers have questioned the fact that mass-impregnated cable is designed on the basis of average voltage stress rather than maximum stress and that comparative long-time load-cycle tests, as previously reported,[2] have shown no appreciable advantages for strand shielding or extra thin paper tape in the high-stress zone. These things appear to contradict theory, which tells us that the high-stress zone near the conductor is critical and needs to be strengthened by such expedients as strand shielding, thin paper tape, and limitations of maximum stress. These things have not proved necessary because of the pronounced ionization self-extinguishing characteristics of this type of cable, which is something that theory does not take into account.

This action may not yet be fully accounted for but recent studies have given a better understanding of it, as will be described. From the beginning, it has been understood that wax formation in the void spaces has had a good deal to do with extinguishing ionization discharge in its incipient stage and before it has had a chance to grow and become cumulative. The puzzling thing has been where all of the surplus compound came from that formed this wax, which completely or partially filled up the void spaces. This was not made clear until lengths of gas-filled cable impregnated with compounds covering a wide range of viscosities were subjected to long-time load-cycle tests and their behavior closely observed. It is now known that if the impregnating compound is of sufficiently low viscosity to be mobile it will be drawn into the high-stress field both by capillary action and by the electric stress field, itself. The voltage stress will also draw this compound into the low-specific-inductive-capacity voids and thus automatically rearrange the distribution of

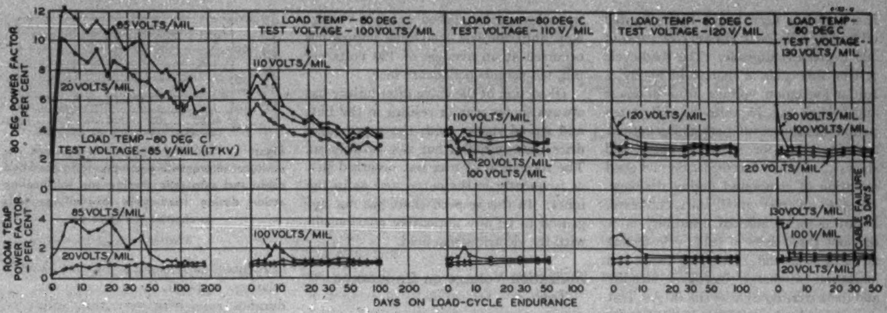

Figure 4. Load-cycle endurance at ten-pounds-per-square-inch nitrogen gas pressure

Three-conductor gas-filled cable of standard construction, with exception that impregnating compound was thin number 5,314 oil having a Saybolt viscosity of 37 at 100 degrees centigrade, insulation thickness 0.200 inch

compound in the cable cross section to those locations where it is most needed. For convenience, this is termed automatic-grading action and the formation of wax, tending to fill up the voids, is termed self-healing action.

Both work together in preventing cumulative ionization in the high-stress zone and, in fact, throughout the whole cable cross section. Dielectric uniformity with absence of "weak spots" has always been an ultimate goal in high voltage cable practice. It is obtained in oil-filled cable by maintenance of oil pressure which, in turn, assures complete absence of voids.[5] In mass-impregnated gas-filled cable it is obtained by controlled gas pressure and the use of a mobile impregnating compound of optimum viscosity. Voids do form but their good behavior is assured by these automatic-grading and self-healing actions.

Load-Cycle Endurance Tests

Cable Impregnated With Thin Oil

These actions were first made clear by the surprising results obtained on load-cycle endurance test with a length of three-conductor cable impregnated with relatively thin number 5,314 oil, such as used in oil-filled cable and having a Saybolt viscosity of 37 at 100 degrees centigrade. The results are charted in Figure 4, the methods of test duplicating in all details those described in the two previous papers.[1,2] The description of test will not be repeated here, except to state that the cable was of the low-pressure shielded type with 350,000-circular-mil unshielded compact sector conductors, 0.200-inch insulation, and single lead sheath.

As would be expected, there was very pronounced drainage of this thin oil from the cable structure before leading, the outer surface having a dry appearance, with only that oil held in the denser parts of the cable cross section by capillary attraction being present. The extreme drainage and relatively large size of voids was verified by the initial power factor measurements at ten pounds per square inch, the initial ionization starting voltage stress being only 48 volts per mil, which is much below average standard results as given in Figure 3.

Figure 4 shows that during the first ten days of the first endurance step at 85 volts per mil there was pronounced ionization and instability. It was so marked that early failure of the cable length was expected. From this time on, however, the cable gradually improved. At the end of 125 days ionization had disappeared, due to closing up of the critical voids. When stability was assured after 189 days the life-test voltage was increased to 100 volts per mil. Ionization and instability were much less marked and disappeared after 95 days, the next voltage step being started after 103 days. These voltage steps were continued with the same results until final failure occurred after 48 days at 130 volts per mil.

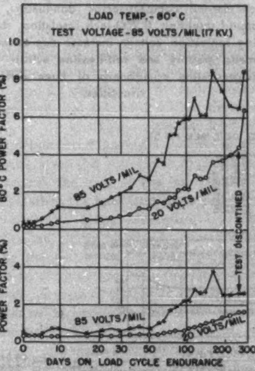

Figure 5. Load-cycle endurance at ten-pounds-per-square-inch nitrogen gas pressure

Three-conductor gas-filled cable of standard construction, with exception that impregnating compound was a heavy hydrogenated oil having a Saybolt viscosity of 1,000 at 100 degrees centigrade, insulation thickness 0.200 inch

The important point to note is that this cable showed even more self-healing ability than standard cable impregnated with heavier oil. Its endurance with final failure at 130 volts per mil, was also as good as the best results obtained with standard cable on these long time tests.

Dissection of the cable after test showed no evidence of the original thin oil. The paper tapes were quite dry and the butt spaces were completely filled with a gummy oil residue, representing that stage of polymerization before final *X* wax is formed. This residue was less gummy and more nearly resembled *X* wax as the conductor was approached. The strand spaces at the unshielded conductor were completely filled with a residue that was practically in the form of final *X* wax. The evidence was quite plain that most of the thin oil left in the paper tapes had been drawn into the low-specific-inductive-capacity voids by electric-field forces and had also been drawn towards the conductor, the paper tapes in the inner zone being less dry than those in the outer zone.

Cable Impregnated With Extra-Heavy Compound

Just the opposite results were obtained with a duplicate length of cable impregnated with a very heavy hydrogenated oil having a Saybolt viscosity of 1,000 at 100 degrees centigrade. The load-cycle test on this cable is charted in Figure 5. Initial ionization voltage after drainage and leading was 75 volts per mil at ten pounds per square inch, which is a normal average value for standard cable and shows good initial impregnation and good retention of compound during drainage. In spite of this good start, however, Figure 5 shows marked instability after the first three days of load-cycle testing at 85 volts per mil. Dielectric losses and ionization discharge increased steadily and there were no signs of stability. Test was, accordingly, continued at 85 volts per mil for 277 days and since there were still no signs of ionization being extinguished, and stability reached, the test was stopped without attempting to go to a higher voltage step.

Dissection of the cable after test showed dry *X* wax deposits in the butt spaces throughout the cross section of insulation. These deposits became more pronounced at the inner layers of paper and at the unshielded conductor surface, showing a relationship to the electrical stress field. The void spaces were not, however, completely filled with this wax.

CABLE IMPREGNATED WITH STANDARD COMPOUND

Typical load cycle endurance charts on gas-filled cable impregnated with standard number 5317 oil, having a Saybolt viscosity of 100 at 100 degrees centigrade were included in the two previous papers[1,2] and will not be repeated here. Initial ionization voltage of standard cable is given in Figure 3. On long-time load-cycle endurance, ionization has always been extinguished at each voltage step until the last, and final failure has occurred at an average of 120 volts per mil, ten pounds per square inch.

Dissection of the cable after failure has always shown wax or residue in the butt and strand spaces, with the paper tapes drier than initially, but not entirely so. The formation of wax has occurred just as frequently in the outer zone as in the inner. In this respect, there has not appeared to be any noticeable relationship with the electric-stress field.

Ionization Voltage on Endurance Test

At intervals of three or four days during the load-cycle endurance runs, just described, power-factor curves were measured over a sufficient voltage range to determine ionization starting voltage at that particular stage of the test. The results with the three different types of impregnating compound already described are charted in Figure 6. These three charts are of particular interest, since they give a direct measurement of critical void size at each stage of the load-cycle test from start to finish, and clearly bring out the automatic-grading and self-healing effects in mass-impregnated gas-filled cable as a function of compound viscosity. Charts *a* and *c* in Figure 6 correspond to Figures 4 and 5, relating, respectively, to load-cycle endurance runs with very thin and very heavy impregnating compounds. Chart *b* in Figure 6 corresponds to Figure 6 in the previous paper,[2] with standard number 5317 impregnating compound of intermediate viscosity.

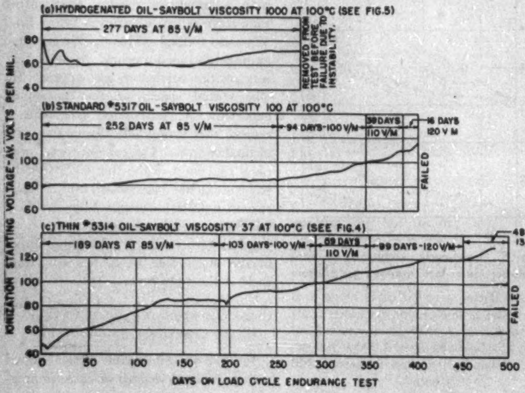

Figure 6. Load-cycle endurance test on three-conductor cable at ten-pounds-per-square-inch nitrogen gas pressure, 350,000-circular-mil compact-sector conductors, no strand shielding, 0.200-inch insulation, showing ionization voltage increase due to automatic grading and self-healing action with impregnating compounds of three different viscosities

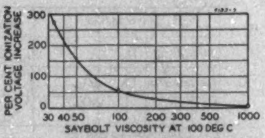

Figure 7. Showing relationship between viscosity of impregnating compound in gas-filled cable and automatic grading and self-healing action during load-cycle overvoltage tests as charted in Figure 6

Chart *a* shows that while very good initial impregnation was obtained with the high-viscosity hydrogenated oil, this cable was unstable from the start and had no self-healing properties at all, ionization starting voltage dropping below the initial value and never fully recovering. After 277 days at 85 volts per mil there was still a pronounced ionization discharge in this cable. It is possible that compounds of other types and of equally high viscosity would give better results, but it is our conclusion that impregnating compounds of extra-high viscosity have little if any self-healing properties in gas-filled cable and are undesirable.

Chart *b* in Figure 6 is typical of standard cable. The cable did not drain objectionably before leading and initial ionization voltage was normal. The cable shows stability from the start, with a steady self-healing action that extinguishes ionization at each voltage step until the last. In no case did ionization voltage drop below the initial value. On the last voltage step cumulative ionization finally predominated and failure occurred after 16 days at 120 volts per mil.

Chart *c* in Figure 6 indicates that while very thin oil imparts remarkable self-healing properties to gas-filled cable there is too much drainage of this oil from the insulation and initial ionization voltage is too low, being only 48 volts per mil at ten pounds per square inch, well below normal operating stress of 65 volts per mil. This caused instability at the start of the load-cycle endurance run, but automatic grading and self-healing action eventually overcame this and the cable settled down to a surprisingly long endurance run, ionization being extinguished at each voltage step until final failure at 130 volts per mil.

Figure 6 plainly shows a relationship between viscosity of impregnating compound and the ability of the cable to extinguish ionization discharge. This is made clearer in Figure 7, where the increase in final ionization voltage, at the end of the endurance run, over the initial value is given as a function of compound viscosity.

Summary of Conclusions

Further laboratory and field studies of gas-filled cable confirm that gas-pressure control gives very close control of performance characteristics.

Over the usual commercial range of conductor sizes and insulation-wall thicknesses, average stress rather than maximum stress determines performance of mass-impregnated gas-filled cable, and it is not necessary to specially strengthen the inner zone of insulation in the high-stress field near the conductor.

Strand shielding of mass-impregnated gas-filled cable has shown no marked benefit but since it is in no way objectionable it can be used when desired.

Extra thin paper tape in the high-stress field has shown no marked benefit in mass-impregnated gas-filled cable and its added cost and complications do not appear justified.

Extra-high-viscosity impregnating compound lacks self-healing properties and should not be used in gas-filled cable.

Thin impregnating oil has remarkable self-healing properties but drains too readily for safe use in gas-filled cable.

Optimum results are obtained with an intermediate impregnating compound of sufficiently high viscosity to be held in place by capillary attraction without draining objectionably but, at the same time, of sufficiently low viscosity to have mobility. Our experience has indicated that a Saybolt viscosity in the order of 100 at 100 degrees centigrade gives the best results.

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A New Frequency Relay for Power-System Applications

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Typical Applications

Loss of Tie-Line Overloading Plant Generation

THE interconnection of large power systems with industrial and defense plants having a comparatively small generating capacity is not unusual in these days. These plants require a considerable block of power for 24-hour full-capacity operation, and this power is available by means of the interconnection with the power system. More than one interconnection would assure higher continuity of service, but the general practice of locating defense plants in rural or suburban areas, remote from urban power-system networks, often results in one transmission-line interconnection operating in parallel with plant generators. Like all lines, the interconnection is subject occasionally to faults with resulting temporary loss of power for the plant. During these emergencies, the local generator or generators at the plant which normally contribute to the total load are suddenly called upon to carry all of the plant load. The local generators are not able to supply this load and consequently are heavily overloaded. As a result, the frequency decreases rapidly. However, complete shutdown of the plant can be avoided by dropping some of the load and retaining within the capacity of the local generators the load of the most essential machines and processes.

Thus, a reliable scheme is required to disconnect the load of the less essential feeders when the interconnection power is lost and the plant generators become overloaded. One scheme in general use is the application of an underfrequency relay in conjunction with reverse power relays. A typical system of this type is illustrated in Figure 1. When the power in the tie line decreases to a minimum, or reverses, and when the generator is overloaded as indicated by a drop in frequency, an auxiliary tripping relay is energized to disconnect the less essential feeders 1 through 4. Then the local generator can supply power to the more important feeders 5 and 6.

At the same time, the auxiliary relay also trips the tie breaker with the power system to prevent feedback of the local power to the power system. In the case of a fault on the interconnection line, the tie-breaker protective relays should complete the trip circuit ahead of the reverse-power and underfrequency relays. In this case, these latter relays operate as backup relays for the tie breaker. When power is available again via the interconnection, the two systems may be resynchronized, and the plant operator can restore the disconnected load in the proper operating sequence.

The measure of plant-system overload in this application is frequency change. The relay should respond to this change with a minimum variation in calibration because of voltage or temperature changes. The relay should be sensitive to frequency changes, operating fast for large changes in frequency and slower for small changes. Time delay is necessary to assure that the frequency relays

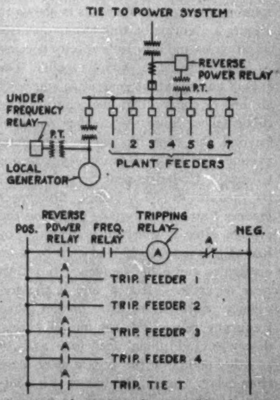

Figure 1. Single-line diagram of local power plant provided with frequency relays for disconnecting load

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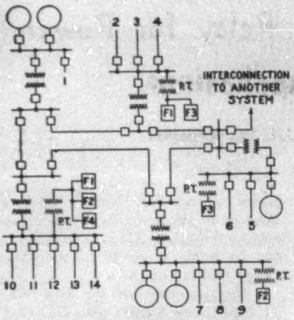

Figure 2. Single-line diagram of power system with frequency relays set for selectivity

Underfrequency Relays
(Normal Frequency—60 Cycles)

Symbol	Setting	Trip
F1	59.5 cycles	4 and 12
F2	59 cycles	9 and 14
F3	58 cycles	2 and 6
F4	57 cycles	10

operate only on sustained frequency changes, and not on changes resulting from faults which last only until the fault is cleared. For these reasons, an induction-type relay with inverse time characteristics is most suitable.

Loss of Generation on a Heavily Loaded System

Frequency relays can be used to disconnect load selectively on an overloaded system. A typical system consisting of several generating stations supplying local as well as remote loads is shown in Figure 2. Normally, this system operates at 60 cycles and near full capacity because of large increase in wartime loads. The loss of an important generating source or tie line or some other emergency system disturbance causes a sudden overload beyond the capacity of the remaining system, and the system frequency begins to drop. Underfrequency relays *F*1, set to operate at 59.5 cycles, disconnect feeders 4 and 12 at remote points on the system after a short time delay. The loss of this amount of load may be large enough to cause the frequency to return near 60 cycles. If not, and if the frequency continues to drop further, then frequency relays *F*2, set for 59 cycles, disconnect additional feeders 9 and 14 at other points on the system after more time delay. Again, the loss of this additional load may be enough to stabilize the frequency, but, if the frequency continues to drop, additional relays with lower-frequency settings disconnect more load. This process can be continued in a number of steps as dictated by the characteristics of the particular system and may be applied in the manner illustrated to drop varying amounts of load at a number of different stations.

Several relays may be set to operate at the same frequency value but to disconnect load at different time intervals. Thus, if the loss of the first block of load does not cause a definite rise in frequency, additional blocks of load would be dropped later, after suitable intervals of time. This has an advantage of only partially disconnecting the load assigned to be dropped at a particular frequency, if the frequency rises between the disconnecting intervals.

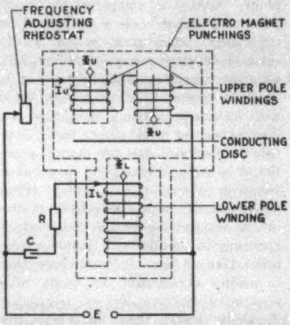

Figure 3. Schematic diagram of frequency relay

These schemes are not expected to replace dispatching or automatic load control, but provide a backup measure that can prove particularly valuable in holding important loads during an emergency. The scheme is applicable particularly to smaller systems such as are commonly found in rural areas, in large industrial plants, or a defense area whose overload capacity is limited, and may be exceeded by unpredicted increases in load.

The frequency relays in this scheme should be sensitive to changes in frequency, yet with a considerable range available for the selection of the operating frequency required in the various steps. In addition to the variable frequency setting, the relays should be capable of variable time settings over a wide range to permit selective timing of the various steps. As the relays may be located at different localities and operate under different conditions, it is desirable that the frequency relay have a minimum temperature and voltage error.

These applications of frequency relays discussed are typical and subject to variations and modifications required in each particular application. Other applications of frequency relays include overspeed or overfrequency protection of rotating equipment as well as underfrequency protection. The relays may not always be used only to trip circuit breakers, but also to give an alarm or warning of impending trouble.

Description of Relay

Relay Operating Principles

The preceding discussion has pointed out the operating requirements of typical systems using frequency relaying. These translate into several specific relay-performance requirements. The minimum trip-frequency variation of the relay should be small with respect to line-voltage change and with respect to ambient-temperature variation. The time and frequency setting of the relay should be easily adjustable over a wide range, and the time delay should be an inverse function of the frequency departure from normal system frequency. The relay described here satisfies these requirements and at the same time is quite simple in mechanical construction. Existing frequency relays use two electrical operating elements with opposing torques. The design described in this paper operates on an entirely different principle and utilizes only a single operating element.

In appearance, the relay is very similar to standard-type induction relays. It has the conventional rotating disk, time-lever scale, damping magnet, and a single electromagnet. Figure 3 is a circuit diagram of relay connections, and Figure 4 is a simplified vector diagram which illustrates the relay operation as either an under- or overfrequency device.

Essentially, the relay operation is comparable to that of a directional element. In Figure 4a, the upper-pole current, I_U, is shown lagging the impressed voltage, *E*. The lower-pole current, I_L, also lags the impressed voltage, but by an angle less than that of I_U. The lower-pole relay circuit, as shown in Figure 3, has inductance and capacitance in series, and, as the frequency increases, the lower-pole current becomes more lagging. Because inductive as well as capacitive reactance is present, the current in this circuit lags at a more rapid rate than the upper-pole current. At some frequency, the lower-pole current will lag sufficiently to be in phase with the upper-pole current. Under such circumstances, the upper and lower fluxes of the relay are in phase, and there will be no torque on the induction disk. If the frequency now increases slightly, the lower-pole current and flux are rotated further in the clockwise direction, so that they lag the upper-pole quantities, and the induction disk will have a torque produced by the quadrature components of the upper- and lower-pole fluxes. The polarity of the overfrequency relay connections is so chosen that for ϕ_L leading ϕ_U, the relay has opening torque; then, when the frequency increases sufficiently for ϕ_L to lag ϕ_U, the torque of the relay reverses so that the contacts move in the closing direction. The larger the frequency increase beyond the zero-torque point, the greater the angular displacement between upper and lower fluxes, and the faster the relay operating time. Therefore, relay timing is inverse with respect to frequency changes.

The design is readily changed to an underfrequency circuit-closing relay by reversing the winding connection of either the upper or lower poles, which reverses the disk torques. To maintain open contact at normal frequency, the circuit constants are adjusted so that I_L lags I_U slightly, as shown by Figure 4b. As the frequency decreases, I_L advances, and, when it leads I_U, the contact closes.

The minimum trip frequency is determined by the point at which I_L and I_U are in phase; that is, zero torque. By changing the angle, with respect to the impressed voltage, at which this condition occurs, the frequency setting of the relay may be set as required. This angle is changed by adjusting the rheostat in the upper-pole circuit. Thus, in the case of the underfrequency relay, Figure 4b, as the resistance is increased, the upper-pole current lags less, so that a larger change in phase angle is required to swing I_L around to lead I_U. This corresponds to a lower minimum trip-frequency setting of the relay. A wide range of adjustment is easily realized by this means. Ten cycles is a typical frequency spread chosen.

RELAY OPERATING CHARACTERISTICS

The principles of relay operation already described permit the use of a single electromagnet and a mechanical construction which is easily adapted to the use of variable time-delay settings. These are obtained by the time-lever adjustments which permit variation of the moving-contact travel from a maximum of 180 degrees of rotation to a very small amount. Typical curves of relay operation for various lever and frequency settings are shown in Figure 5. These are taken for an instantaneous frequency drop. The wide range of settings available gives wide application limits.

The type of element described here inherently meets the requirement of minimum variation in tripping frequency with variation in impressed voltage. This may be seen if the relay is considered as a sensitive directional element operating near zero torque. At exactly zero torque, change in voltage will have no effect. The torque remains zero. Close to zero torque, the effect of voltage change on the relay torque is still negligible, as shown in Appendix I.

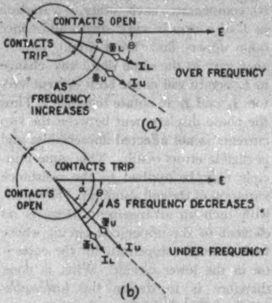

Figure 4. Vector diagrams showing principles of relay operation

The relay is extremely sensitive to frequency change. Consider the following equation for relay torque, and rate of change of torque

$$T = KI_LI_U \sin(\theta-\alpha) \qquad (1)$$

$$\frac{dT}{d(\theta-\alpha)} = KI_LI_U \cos(\theta-\alpha) \qquad (2)$$

where K is a factor of proportionality, and $\theta-\alpha$ is the phase angle between I_L and I_U. At zero torque, $\theta-\alpha=0$. At this point the rate of change of relay torque with respect to phase angle is a maximum. Since $\theta-\alpha$ is a function of the frequency only, the relay will be at maximum sensitivity to frequency change.

The circuit elements of the relay are designed to make optimum use of the inherent sensitivity to frequency change and insensitivity to voltage change. The factors which determine the optimum design are the phase angle of the upper-pole circuit and the inductance and capacitance of the lower-pole circuit. The phase angle of the upper-pole circuit determines the angle at which zero torque occurs. The two extremes in design, occur for an angle of 0 degrees and an angle of 90 degrees. For the first case a large amount of external resistance is required to swamp out the inductive reactance of the relay coil, and hence only a small amount of operating ampere turns would be supplied the upper-pole windings. That is, the relay would not be a very sensitive directional element, so that this type of design is not satisfactory. The other extreme is for zero torque to occur at 90 degrees lag. Such a condition requires a purely reactive circuit, and the phase angle of such a circuit does not vary with frequency change. Thus, if any attempt were made to operate the relay with a zero-torque angle occurring near 90 degrees, the performance would be very unsatisfactory, since relay torque would respond in only small degree to change in frequency. It is apparent, therefore, that the phase angle of zero torque should be somewhere between these two extremes. An angle of 55 degrees is in fact the optimum one, and the relay is designed to operate near this value. This is more rigorously shown in Appendix II. With 55 degrees as the optimum zero-torque angle, the relative values of upper-pole circuit resistance and inductance are determined to be $R_U=0.707\ X_U$.

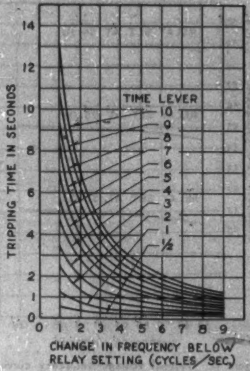

Figure 5. Typical time curves of 115-volt 50–60-cycle underfrequency relay

The following considerations apply to the determination of the lower-pole-circuit constants. A circuit is required which changes vary rapidly in phase angle with respect to change in frequency. This is the case when a resistor is used in conjunction with reactive impedance whose magnitude varies very rapidly with frequency change. Such an impedance may be the series combination of inductance and capacitance. The re-

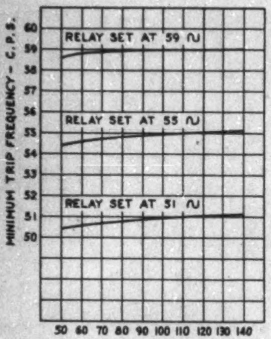

Figure 6. Typical voltage–frequency curves of 115-volt 50–60-cycle underfrequency relay

actance of such a combination is the difference $X_L - X_C$. Since X_L and X_C are inverse with respect to frequency variation, the difference will vary very rapidly if both X_L and X_C are very large and nearly equal. That is, the inductance of the lower-pole circuit should be as large as possible, and the capacitance as small as possible consistent with permissible burden and heating requirements. The resistance of this circuit is chosen approximately 0.707 times the net reactance so that the 55 degrees zero-torque-angle requirement is satisfied. Appendix II presents a more formal derivation of these requirements.

The variation of minimum trip frequency with respect to voltage change for a relay designed on the basis of the preceding criteria, is shown in Figure 6. For the complete range of settings, the frequency error is negligibly small for voltage variations from 50 to 140 volts. Note however that the errors are greatest at the low-frequency settings. Here the largest value of upper-pole circuit resistance is required, and this represents the maximum departure from the optimum circuit requirements previously set forth.

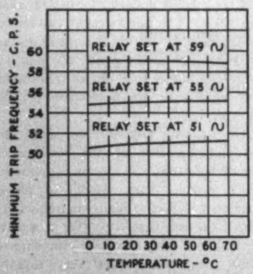

Figure 7. Typical temperature–frequency curves of 115-volt 50–60 cycle underfrequency relay

The last consideration in determining the optimum design of the relay-circuit elements is the error due to ambient-temperature variation. This error can occur because of the variation of the inductive, capacitive, and resistive elements of the relay, with temperature. Since these variations cannot be eliminated entirely, the circuits must be designed so that the various changes tend to compensate each other. Variation in L of course is very small, and, since both upper- and lower-circuit inductance vary at the same time, any change in L merely will cause both current vectors $\boldsymbol{I}_L$ and $\boldsymbol{I}_U$ to rotate together. Thus the phase displacement between the two currents is not affected appreciably, and negligible errors result. This same principle may be applied to the resistance variation of the coil windings. However, with such an arrangement, there is no element in the upper coil circuit whose variation will compensate for the capacitor in the lower circuit. What is done therefore is to design the lower-pole winding with as small a coil resistance as possible relative to the upper-pole coil resistance. The physical construction of the electromagnet makes this relatively simple. The additional resistance placed in series in the lower-pole circuit to give optimum power factor produces a swamping action so that the over-all temperature variation of resistance in the lower-pole circuit is small. As the upper-pole resistance varies with temperature change (assuming negligible change in lower-pole circuit resistance) the vector $\boldsymbol{I}_U$ is swung around slightly. The direction is more lagging if the temperature decreases and less lagging if the temperature increases. C, in Figure 4, is a capacitor with a small negative temperature coefficient. That is, as the temperature decreases, C increases and X_C decreases. As the temperature increases, X_C increases. Thus $\boldsymbol{I}_L$ swings in the lagging direction as the temperature decreases and in the leading direction as the temperature increases. That is, the lower-pole current vector follows the upper-pole circuit vector, and hence the phase angle between the two changes but slightly with temperature variation, so that the frequency error due to ambient change is small. Typical temperature curves for a relay designed as described are shown in Figure 7. These indicate that the temperature error has been eliminated to a large extent.

CONCLUSIONS

This paper describes a new frequency relay with inverse time delay which trips at an assigned frequency with negligibly small voltage and temperature errors. The time delay of the relay, as well as the minimum trip frequency may be independently adjusted within a wide band. These design characteristics make the relay very suitable for power-system applications in which frequency is used as the discriminating quantity.

Appendix I

The following derivation indicates the order of magnitude of relay torque variation with respect to voltage near minimum trip frequency.

List of Symbols

E = voltage impressed on relay
R_L, L_L, C_L = resistance, inductance, capacitance of relay lower-pole circuit
R_U, L_U = resistance, inductance of relay upper-pole circuit
X_L = lower-pole circuit reactance $= \omega L_L - \dfrac{1}{\omega C_L}$
X_U = upper-pole circuit reactance $= \omega L_U$
$\dot{Z}_L$ = lower-pole circuit impedance $= R_L + j\left[\omega L_L - \dfrac{1}{\omega C_L}\right]$
$\dot{Z}_U$ = upper-pole circuit impedance $= R_U + j\omega L_U$
I_L = lower-pole current
I_U = upper-pole current
θ = phase angle by which I_L lags E
α = phase angle by which I_U lags E
$\omega = 2\pi F$
F = minimum trip frequency
T = relay torque
K = constant of proportionality

Assuming negligible shift between relay fluxes and respective currents, the equation of the induction disk torque is as follows:

$$T = KI_UI_L \sin(\theta - \alpha) \tag{3}$$

where $\theta - \alpha$ is the angle between I_U and I_L. Note that for $\theta > \alpha$, T is positive; $\theta < \alpha$, T reverses. However, only the absolute value of $\theta - \alpha$ is considered in this discussion, so that it is equally valid for under- or over-frequency relays.

$$\theta = \tan^{-1}\frac{X_L}{R_L} = \tan^{-1}\frac{\omega L_L - \dfrac{1}{\omega C_L}}{R_L} \tag{4}$$

$$\alpha = \tan^{-1}\frac{X_U}{R_U} = \tan^{-1}\frac{\omega L_U}{R_U} \tag{5}$$

$$I_L = \frac{E}{Z_L} = \frac{E}{\sqrt{R_L^2 + \left(\omega L_L - \dfrac{1}{\omega C_L}\right)^2}} \tag{6}$$

$$I_U=\frac{E}{Z_U}=\frac{E}{\sqrt{R_U^2+\omega^2L_U^2}} \tag{7}$$

The variation of torque with respect to voltage is given by

$$\frac{\partial T}{\partial E}=\frac{\partial}{\partial E}\left[\frac{KE^2}{Z_UZ_L}\sin(\theta-\alpha)\right] \tag{8}$$

$$\frac{\partial T}{\partial E}=\frac{2EK}{Z_UZ_L}\sin(\theta-\alpha)$$

$$\frac{\partial T}{\partial E}=\frac{2KI_UI_L\sin(\theta-\alpha)}{E}=\frac{2T}{E} \tag{9}$$

At minimum trip, the torque is only that amount required to overcome relay spring tension. Thus the numerator of equation 9 is small and the denominator large, so that the variation of torque with respect to voltage is small.

Appendix II

The following derivations indicate optimum design requirements for the relay circuit elements:

The relay sensitivity is measured by the magnitude of torque variation with respect to ω. This should be as large as possible.

If we refer to equation 3

$$\frac{\partial T}{\partial\omega}=\frac{\partial T}{\partial I_L}\cdot\frac{\partial I_L}{\partial\omega}+\frac{\partial T}{\partial I_U}\cdot\frac{\partial I_U}{\partial\omega}+\frac{\partial T}{\partial(\theta-\alpha)}\cdot\frac{\partial(\theta-\alpha)}{\partial\omega} \tag{10}$$

From equations 4, 5, 6, 7, $\partial(\theta-\alpha)/\partial\omega$, $\partial I_U/\partial\omega$ and $\partial I_L/\partial\omega$ are evaluated. From equation 3, $\partial T/\partial I_L$, $\partial T/\partial I_U$, and $\partial T/\partial(\theta-\alpha)$ are determined.

Collecting terms

$$\frac{\partial T}{\partial\omega}=KI_LI_U\cos(\theta-\alpha)\times\left[\frac{R_L\left(L_L+\frac{1}{\omega^2C_L}\right)}{Z_L^2}-\frac{R_UL_U}{Z_U^2}\right]-KI_LI_U\sin(\theta-\alpha)\times\left[\frac{X_L\left(L_L+\frac{1}{\omega^2C_L}\right)}{Z_L^2}+\frac{X_UL_U}{Z_U^2}\right] \tag{11}$$

At minimum trip $\theta-\alpha$ is small, so that the second term of equation 11 may be neglected. Hence if we set $\cos(\theta-\alpha)=1.0$, and note that at minimum trip

$$\cos\theta=\cos\alpha=\frac{R_U}{Z_U}=\frac{R_L}{Z_L} \tag{12}$$

we have

$$\frac{\partial T}{\partial\omega}=KI_LI_U\cos\theta\left[\frac{L_L+\frac{1}{\omega^2C_L}}{Z_L}-\frac{L_U}{Z_U}\right] \tag{13}$$

$\partial T/\partial\omega$, and hence the frequency sensitivity can be made large by choosing L_L large, C_L small, and Z_L small. That is, the inductive reactance ωL_L, and the capacitive reactance $1/\omega C_L$, must both be large, but the resultant reactance $X_L=\omega L_L-1/\omega C_L$ should be small. This will mean that

$$\frac{L_U}{Z_U}\ll\left[L_L+\frac{1}{\omega^2C_L}\right]\frac{1}{Z_L} \tag{14}$$

The optimum value of R_L is determined by differentiating $\partial T/\partial\omega$, the measure of relay sensitivity, and setting the derivative equal to zero. This determines maximum sensitivity with respect to R_L.

If we substitute equations 6 and 12 in equation 13

$$\frac{\partial T}{\partial\omega}=\frac{KEI_UR_L}{Z_L^3}\left[L_L+\frac{1}{\omega^2C_L}\right]-\frac{KEI_U}{Z_U}\frac{R_LL_U}{Z_L^2}$$

$$\frac{\partial^2T}{\partial\omega\partial R_L}=KEI_U\left(L_L+\frac{1}{\omega^2C_L}\right)\times\left[\frac{1}{(R_L^2+X_L^2)^{3/2}}-\frac{3R_L^2}{(R_L^2+X_L^2)^{5/2}}\right]-\frac{KEI_UL_U}{Z_U}\left[\frac{1}{R_L^2+X_L^2}-\frac{2R_L^2}{(R_L^2+X_L^2)^2}\right]=0$$

$$\left[L_L+\frac{1}{\omega^2C_L}\right]\left[\frac{Z_L^2-3R_L^2}{Z_L^5}\right]-\frac{L_U}{Z_U}\left[\frac{Z_L^2-2R_L^2}{Z_L^4}\right]=0$$

$$R_L^2=Z_L^2\left[\frac{\left(L_L+\frac{1}{\omega^2C_L}\right)-L_U\frac{Z_L}{Z_U}}{3\left(L_L+\frac{1}{\omega^2C_L}\right)-2L_U\frac{Z_L}{Z_U}}\right]$$

$$\cos\theta=\cos\alpha=\frac{R_L}{Z_L}=\sqrt{\frac{\left(L_L+\frac{1}{\omega^2C_L}\right)-L_U\frac{Z_L}{Z_U}}{3\left(L_L+\frac{1}{\omega^2C_L}\right)-2L_U\frac{Z_L}{Z_U}}}$$

$$\cos\theta=\cos\alpha=\sqrt{\frac{1}{3}}\sqrt{\frac{1-\frac{L_U}{Z_U}\cdot\frac{Z_L}{\left(L_L+\frac{1}{\omega^2C_L}\right)}}{1-\frac{2}{3}\frac{L_U}{Z_U}\cdot\frac{Z_L}{\left(L_L+\frac{1}{\omega^2C_L}\right)}}} \tag{15}$$

But from equation 14

$$\frac{L_U}{Z_U}\ll\frac{L_L+\frac{1}{\omega^2C_L}}{Z_L}$$

or

$$\frac{L_U}{Z_U}\cdot\frac{Z_L}{L_L+\frac{1}{\omega^2C_L}}\ll 1$$

Therefore, the second radical of equation 15 is approximately equal to 1, and we have

$$\cos\theta=\cos\alpha\approx\sqrt{\frac{1}{3}}$$

$$\theta=\alpha\approx 54.7° \tag{16}$$

Therefore, $\tan\theta=1.414$

or

$$\sqrt{2}R_L=X_L=\omega L_L-\frac{1}{\omega C_L} \tag{17}$$

To indicate the accuracy of this approximation, suppose

$$\frac{L_U}{Z_U}\cdot\frac{Z_L}{L_L+\frac{1}{\omega^2C_L}}=0.25$$

which means that

$$\left(L_L+\frac{1}{\omega^2C_L}\right)\frac{1}{Z_L}$$

is only a nominal amount larger than L_U/Z_U. Even under these circumstances, the value of θ calculated from equation 15 is 56.7 degrees, which indicates that the approximated value of 54.7 degrees is very close for design purposes.

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Modern Motive Power for the Sorocabana Railway

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THE Sorocabana Railway in inaugurating electric traction on its extensive meter-gauge system has selected the 87-mile-long double-track section between São Paulo and Santo Antonio in the State of São Paulo, Brazil, for the initial electrification. Twenty locomotives are being furnished to handle passenger, freight, and mixed trains with scheduled speeds of 35 miles per hour for passenger, and 30 miles per hour for freight and mixed trains over a mountainous terrain involving maximum grades of two per cent, and prevailing minimum radius curves of approximately 800 feet.

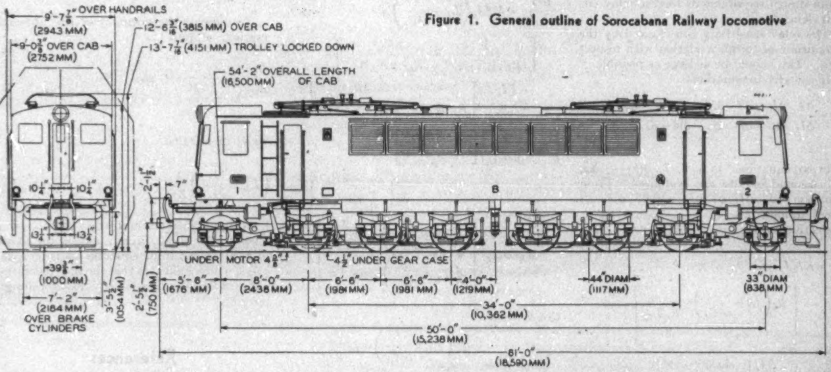

Figure 1. General outline of Sorocabana Railway locomotive

These locomotives are the largest and most powerful 3,000-volt d-c meter-gauge units built to date and are among the outstanding examples of design and construction embodying all welded fabrication of mechanical parts and assemblies for main-line passenger and freight locomotives. Ten are being furnished by the General Electric Company and ten by the Westinghouse Electric and Manufacturing Company, with the mechanical portion of all 20 built by the General Electric Company. In addition to the mechanical portions which are identical, much of the equipment as furnished by the two companies is either identical or physically and electrically interchangeable. Pantographs, traction-motor blowers, watt-hour meters, and high-speed *JR* circuit breakers are identical with all pantographs manufactured by the Westinghouse Company and all breakers manufactured by the General Electric Company. Traction motors and motor generator blower sets are interchangeable. General characteristics of the locomotives are given in Table I. General dimensions are shown by Figure 1, and general appearance by Figure 2.

Running Gear

The running gear consists of two three-axle driving trucks (Figure 3) and two single-axle radius-bar guiding trucks (Figure 4). The driving-truck frames are articulated at their inner end by a ball joint arranged to permit angular movement in both vertical and horizontal planes. The outer ends extend over the guiding trucks and carry draft gear, coupler, and pilot. The frames are fabricated of steel plates and structural shapes throughout with all parts torch-cut to size requiring little or no machining (Figure 5). Side frames are universal mill-rolled open-hearth steel $3^1/_2$ inch thick, normalized, with an ultimate tensile strength of 55,000–65,000 pounds, maximum carbon content 0.25 per cent.

The guiding trucks are equipped with outside journals and rolled-steel wheels. Semielliptic and coil springs are arranged in series over the top of the journal boxes. A laterally movable bolster is provided on which a portion of the locomotive weight is carried through a system of equalizers extending from the springs over the outer driver boxes. The bolster is provided with a variable gravity-restraint device of relatively high initial restraint to steady the locomotive and minimize lateral oscillations when it is running on tangent track and to assist guiding of drivers negotiating track turnouts and curves (Figures 6 and 7). An extension of each guiding-truck inner frame serves as a radius bar connecting it to the main truck frame. These truck frames are also fabricated from torch-cut steel plates and structural shapes readily adapted to welding. The weight of the locomotive, with the exception of wheels, axles, journal boxes, and that part of the motors which is carried directly on the axles, is carried on a system of semielliptic and coil springs equalized to provide the desired loading on all wheels (Figure 8).

Cab Underframe

The cab underframe or platform is constructed of commercial steel shapes, bars, and plates. The structure is about 54 feet long by 9 feet wide. The center sills, which are the main load-carrying members of the frame, consist of two 18-inch

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Figure 2. Three-quarter view of Sorocabana Railway locomotive

Figure 3 (left). Assembly of driving and guiding trucks complete

Figure 4 (below). Guiding truck

Figure 5. (right) Driving-truck frame

by 12-inch H beams reinforced with top and bottom plates of one-half inch thickness, forming a central duct serving as a pressure chamber into which air is discharged from two motor-operated blowers. From this chamber ventilating air is distributed to traction motors, accelerating resistors, and other electric apparatus. Side and end sills are made of eight-inch standard steel channels. Bolsters supporting center plates, side bearings, and lifting lugs are rigidly tied into center- and side-sill members. The 18-inch sill members extend well beyond the bolsters and are joined with eight-inch H beams (Figure 9) extending to ends of frame.

Cab

The cab is of semistreamlined design and is carried on two center plates located one on each of the two driving trucks. One of the center plates is arranged for sliding motion in the lower or truck socket to compensate for variations in truck center-plate distance when operating on curved track. Additional supports are provided through four spring-cushioned loading pads slideably supported, one on each side of each driving truck. The loading-pad springs are calibrated to give the desired load distribution on all driving and guiding wheels.

The cab consists mainly of exterior sheets 0.180-inch thick welded to a four-inch steel channel frame erected on the cab underframe. Louvres are provided in the side sheets of the central portion of the cab for ingress of air to the motor-blower sets. Large hatch openings are provided in the roof to facilitate installation and removal of apparatus and are closed by means of bolted-down hatch covers.

Apparatus Compartment

The apparatus compartment is built as a separate unit to facilitate easy installation of control apparatus and wiring. After the installation of apparatus and wiring is complete, the compartment is lowered through the hatch opening in the cab and is welded securely to the underframe and roof structure.

Table I. Characteristics of General Electric Locomotives for Sorocabana Railway

Classification 1−*C*+*C*−1−238/286−6 *GE* 734 *A*	3,000 volts
Length over-all, inside of coupler knuckles	61 feet, 0 inches
Width over-all	9 feet, 7$^7/_8$ inches
Height over trolley locked down	13 feet, 7$^7/_{16}$ inches
Track gauge	1 meter
Total wheel base	50 feet, 0 inches
Rigid wheel base	13 feet, 0 inches
Diameter driving wheels	44 inches
Diameter guiding wheels	33 inches
Total weight in running order	286,000 pounds
Weight on drivers	238,000 pounds
Weight per driving axle	39,666 pounds
Weight on guiding axles	48,000 pounds
Weight per guiding axle	24,000 pounds
Number of motors	6
Gear ratio	75/17
Continuous rating (horsepower)	1,910
One-hour rating (horsepower)	2,195
Continuous tractive effort (pounds)	24,475
One hour tractive effort (pounds)	29,500
Speed, continuous rating (miles per hour)	29.3
Speed, one-hour rating (miles per hour)	27.9
Maximum safe speed (miles per hour)	56

Construction Details

The locomotive design incorporates throughout the use of fabricated sub-

Figure 6. Guiding-truck bolster, center-plate extension, and parts of lateral-restraint device

Figure 7. Guiding-truck frame

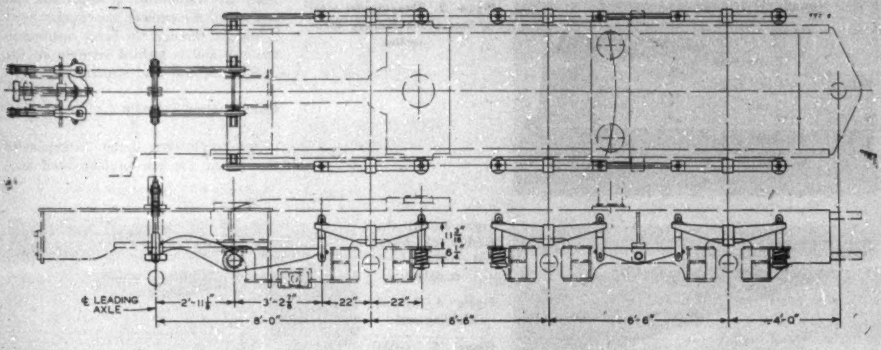

℄ LEADING AXLE
2'-11⅛"
3'-2⅞"
22"
22"
8'-0"
6'-6"
6'-6"
4'-0"
11 2/16"
6 1/4"

Figure 8. Spring and equalization system

assemblies which are easily handled for operations on the individual parts before being welded together for the complete assembly. All cross-tie plates fitting between side frames are machined to length with ends squared and are welded into subassemblies to facilitate the easy setting up and squaring of the main truck frames. Each side frame, the articulation or inner end frame, the main-frame extension and other similar groups of parts are likewise fabricated to form finished subassemblies before final assembly together. The boring of the recess for the cab center plate in the middle cross tie and also the boring of the inner end frames for the articulation are performed in the subassembly stage.

Figure 9. Cab underframe

After the truck frame is otherwise assembled and welded, pedestal shoes, forming guides for journal boxes, are set to position in a special fixture. Shoes are accurately trammed to position for squareness and distance, laterally as well as longitudinally, to the frame center line extending through articulation, cab center plate, guiding-truck radius-bar fulcrum and center plate, draft gear and coupler. After accurate adjustments are accomplished the shoes are welded rigidly to the pedestal brackets. The cab sides, together with the cab frame, the hatches, cabinets, and compartments, likewise are first fabricated as subassemblies after which they are set up and assembled with the cab platform which is itself a complete subassembly.

Figure 11. Apparatus-compartment unit

Air-Brake Equipment

The air-brake equipment is a combination of vacuum and compressed-air systems with the locomotive brakes operated by compressed air and the train brakes operated by vacuum. The locomotive brakes may be operated independently of the train brakes, or in conjunction with them, in which latter case the train vacuum system acts as a pilot for the locomotive air-brake system. Two 12-inch-by-12-inch brake cylinders on each truck apply the brakes on that truck through a single brake shoe on each driving wheel. Interlocking is provided to prevent simultaneous operation of air brakes and regenerative braking to avoid

Figure 10. General arrangement of apparatus in cab

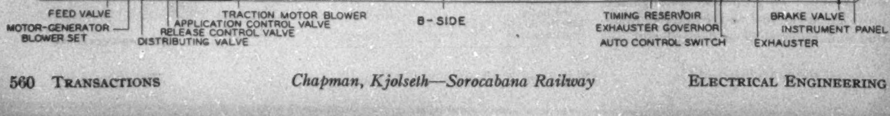

INSTRUMENT PANEL
BRAKE VALVE
CONTROLLER
NO.1 END
COMPRESSOR
COMPRESSOR GOVERNOR
VOLUME RESERVOIR
A-SIDE
MOTOR FIELD SHUNTS
EXHAUSTER
MOTOR-GENERATOR BLOWER SET
TRACTION MOTOR BLOWER
WATTHOUR METER
CONTROLLER
NO.2 END
SEAT
SAND BOX
CIRCUIT BREAKER
CONTACTORS
RESIS-TORS
RESISTORS
SERIES PARALLEL SWITCH
SAND BOX
SEAT
HAND-BRAKE
CONTROL COMPARTMENT
REVERSER
BRAKING SWITCH
FUSE BOX
RESISTORS
SEAT
SEAT
SAND BOX
LIGHTNING ARRESTOR
RESISTORS
CONTACTORS
SAND BOX
FEED VALVE
MOTOR-GENERATOR BLOWER SET
TRACTION MOTOR BLOWER
APPLICATION CONTROL VALVE
RELEASE CONTROL VALVE
DISTRIBUTING VALVE
B-SIDE
TIMING RESERVOIR
EXHAUSTER GOVERNOR
AUTO CONTROL SWITCH
BRAKE VALVE
INSTRUMENT PANEL
EXHAUSTER

Figure 12. Traction-motor, wheel, and axle unit

the possibility of wheel sliding during regenerative braking.

Location of Apparatus

The space between the operator's cabs is divided into three sections. The apparatus compartment, completely enclosed but with access doors and with removable covers along the aisles on each side, is in the center, while auxiliary apparatus such as motor generator blower sets, compressors, exhausters, and air-brake control devices are located at each end. The operator's cabs at each end of the locomotive are separated from the apparatus compartment by bulkheads with a door opening onto an aisle on each side of the locomotive (Figure 10). All apparatus is arranged to provide maximum accessibility for inspection and maintenance. Figure 11 shows the apparatus compartment complete with all equipment being lowered into place in the main cab through the hatch opening in the roof.

Electric Apparatus

Two spring-raised air-lowered pantographs are provided, either of which has adequate capacity for collecting total locomotive current from the 3,000-volt overhead line. The pantographs are equipped with antifriction bearings, and each has two separate contact shoes, independently hinged, to provide greater flexibility in maintaining contact with the overhead contact wire. A small reservoir which can be charged from the control air system is provided for maintenance of air pressure for unlatching the pantograph when both pantographs are down and no other air pressure is available on the locomotive. A small hand-operated air pump is also provided for this purpose. The two pantographs are connected together through a cable mounted on insulators on the roof and to the main power circuits through the protective devices mounted in the apparatus compartment in the main cab.

The traction motors are wound and insulated for operation two in series on 3,000 volts and are of the commutating-pole force-ventilated type with antifriction bearings. Figure 12 shows a traction motor in place and suspended on the axle with gear case in place. Axle-suspension bearings are waste-packed, oil-lubricated. The drive consists of single-reduction gearing with a pinion on the motor shaft engaging a solid gear on the axle, both being enclosed in a gear case which also serves as a reservoir for the gearing lubricant.

The motor circuits are arranged for three combinations of motors: six in series, three in series with two such groups in parallel, and two in series with three such groups in parallel. Accelerating steps and one full-field and two reduced-field running steps are provided for each motor combination, thus making a total of nine running steps in motoring (Figures 13 and 14). Regenerative braking is provided for each of the three combinations of motors (Figures 15 and 16).

The auxiliary equipment duplicates that previously used on locomotives for the Paulista Railway, and consists of two 3,000/65-volt motor generator blower sets, two 65-volt exhausters, and one 65-volt compressor.

Each motor generator blower set consists of a single-commutator series-wound commutating-pole 3,000-volt motor, direct-connected to a 65-volt generator with a blower rotor overhung on an extended shaft from the motor end of the set. The generators of the two sets furnish power for the operation of control, lights, compressor, and exhausters, with one of the sets serving as an exciter

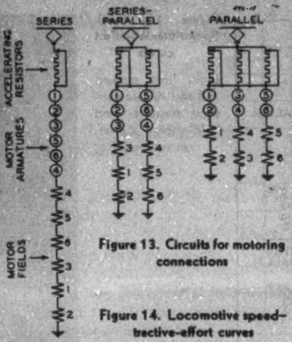
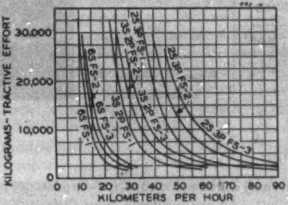


Figure 13. Circuits for motoring connections

Figure 14. Locomotive speed–tractive-effort curves



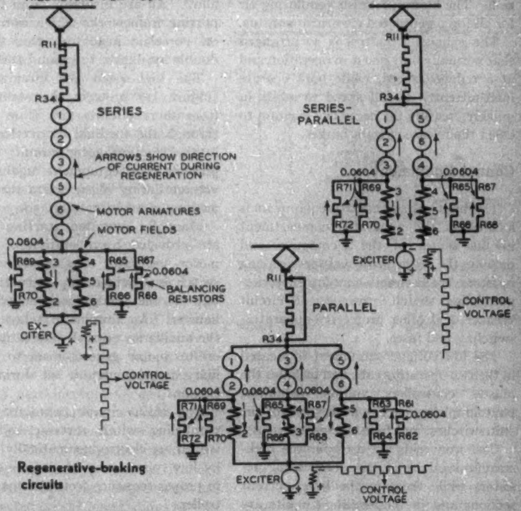


Figure 15. Regenerative-braking circuits

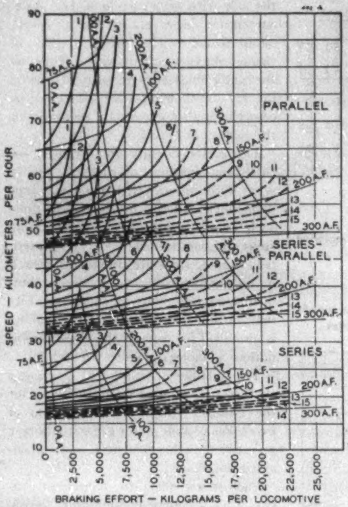


Figure 16 (left). Speed-braking-effort curves

Light lines marked *AA* and *AF* are constant armature- and field-ampere curves, respectively

Figure 17(right). *JR* circuit breaker

for traction-motor fields during regeneration. The blowers furnish ventilating air for all force-ventilated electric apparatus.

The exhauster control is so arranged that normally only one is in operation and at a reduced speed, while both operate intermittently at full speed to assist in quickly creating the necessary vacuum to effect the release of train brakes.

Control Equipment

The high-voltage control equipment is located in the apparatus compartment in the center of the locomotive and includes the accelerating resistors, braking resistors, main-circuit contactors, series-paralleling switch, reverser, *JR* circuit breaker, and other protective apparatus, switches, and fuses.

The low-voltage equipment is located in the two operating cabs and includes the master controllers, voltage regulator, pantograph magnet valves, control-circuit switches, and fuses and instruments.

Cast iron grids are used for the high-current sections of the accelerating resistors while those in the lower-current sections and in the braking circuits are formed of edge-wound strips of resistance alloy. All are insulated from their supporting frameworks which are mounted on porcelain insulators, thus providing double insulation to ground throughout.

The high-speed *JR* circuit breaker (Figure 17) protects the main circuits from short circuits and from overload through the medium of overload relays in the traction-motor circuits and also protects the equipment against overvoltage during regeneration through the medium of an overvoltage relay.

Manually operated starting switches are provided for operation of the two motor generator sets, and a selector switch is provided for alternative selection of the continuously operated exhauster. An automatic relay transfers the auxiliaries normally operating on the exciter motor generator set to the auxiliary motor generator set during regeneration.

The main circuit contactors, series-paralleling switch, reverser, and braking switch are electropneumatically operated by low-voltage control circuits energized in proper sequence from the master controller.

Each master controller is equipped with a main handle controlling accelerating and full-field running steps in motoring, a selector handle for selecting motor combinations for regenerative braking, a braking handle governing both regeneration and shunt-field steps in motoring, and a reverser handle for selecting direction of motion of the locomotive. All handles are interlocked mechanically to prevent improper operation and are conveniently located and in easy reach of the operator.

The instrument panel is mounted beneath the front window, directly in front of the engineer, and contains a line voltmeter, traction-motor field ammeter, traction-motor line ammeter, locomotive speed indicator, vacuum-brake gauge, and an air-brake gauge, illuminated indirectly from the rear of the panel.

The air- and vacuum-brake valves are on the right of the operating position, the master controller on the left. Bell ringer and sander valves, control push buttons and valve for the pneuphonic horn are all mounted so as to require a minimum of movement on the part of the engineer. A record of power consumed during motoring and of power returned to the line when regenerating is obtained by means of a duplex watt-hour meter.

Voltage Transients in Arc-Furnace Circuits

AIEE SUBCOMMITTEE ON TRANSIENT VOLTAGES IN ARC-FURNACE CIRCUITS

THE subcommittee on transient voltages in arc-furnace circuits was established about four years ago to investigate the magnitude, cause, and control of transient voltages occurring at the primary and secondary terminals of arc-furnace transformers. The subcommittee has sponsored a number of field investigations during the past four years with the co-operation of steel companies, manufacturers of furnace equipment, and electric-equipment manufacturing companies. Only a part of the data obtained has been made generally available through publication (see a paper by Z. R. Whitehouse and C. C. Levy, "Surge-Voltage Tests on Electric Steel Arc Furnaces," presented at a joint meeting of AIEE and Association of Iron and Steel Engineers in Canton, May 1941, and also published references 1, 2, and 6). A data group of the subcommittee (L. W. Clark, chairman; S. B. Griscom, and E. R. Whitehead) was instructed to assemble all of the data available from the several field investigations, correlate the data as completely as possible to make them available for further study by the subcommittee, and prepare general conclusions regarding the magnitude of voltage transients found during the investigations. This paper summarizes the work of the data group and the preliminary findings of the subcommittee. It should be emphasized that this paper forms only a preliminary report of the work to date and is presented only so that the field data may be available for further study and analysis. The work of the subcommittee is continuing, not only in the collection and analysis of field data but also in experimental laboratory investigations and analytical analyses. It is expected that more definite recommendations regarding operating practices and possible protection schemes will be made after further study.

Installation Data

Table I lists the essential information concerning the 15 furnaces tested at 12 different locations, with references to the key-system diagram in Figure 1. The rated temperature rise as well as the kilovolt-ampere rating of the furnace transformer has been listed in order that the true thermal rating of the transformer be known. All of the installations except two were 55-degree-rise transformers. The two transformers rated at 40 degrees and 45 degrees rise, respectively, are thus proportionately larger than those with the same kilovolt-ampere rating at 55 degrees rise.

Transformers for the most generally used single-phase arc furnaces are in the 125-volt class, single-voltage operation, voltage range 90–125 volts.

Transformers for three-phase arc furnaces are in the 250-volt class, multiple-voltage operation. The maximum voltages are within the range 220 to 300 volts. Although the maximum voltages of large units are seldom below 250 volts, the voltages in the upper part of the range given are often used for small units. The selection of maximum voltage does not follow a size classification.

The grounding practice appears to be practically uniform. In all cases except one, the transformer tank and furnace shell are grounded and the secondary circuit is grounded through lamps. The data collected on the secondary-bus installations were incomplete, and only at 3 out of 12 installations was the actual spacing of the bus obtained.

Table II lists all essential data regarding the power supply to the furnace transformer. Sufficient information has been collected on the installations so that it should be possible to estimate the capacitance to ground of that part of the power-supply system directly affecting the furnace operation. Several of the tests indicate that the characteristics of the power-supply system may have a considerable effect on the magnitude and number of voltage transients experienced. It is desirable to have the power-supply data available for further study as to its possible effect upon the transients.

An indication is given as to whether or not there have been insulation failures on either the line or transformer side of the transformer breaker, or on the secondary bus. However, there is no definite assurance that voltage transients have caused each of these failures.

Test Data

Table III gives most of the test data collected at the different installations. The recording device in most cases was a Lichtenberg figure recorder which records both the magnitude and number of transients. In one of the tests a cathode-ray oscillograph was used, and in another test a vacuum-tube counter was used to record the number of transients that occurred above a certain magnitude.

Most of the tests were made with the recorder on the transformer side of the furnace breaker, but in a few cases the recorder was on the line side of the furnace breaker. Also, in most cases the recorder was connected line to ground, although

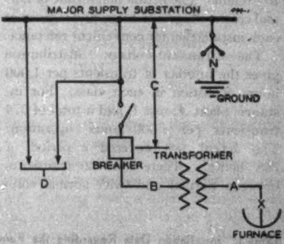


Figure 1. Schematic diagram of arc-furnace installation for reference—from Table II

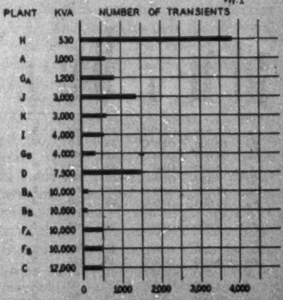


Figure 2. Total number of transient voltages per 1,000 hours of furnace operation plotted against furnace-transformer size

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Paper 44-99, recommended by the AIEE committee on electrochemistry and electrometallurgy for presentation at the AIEE summer technical meeting St. Louis, Mo., June 26–30, 1944. Manuscript submitted April 29, 1944; made available for printing May 8, 1944.

Personnel of subcommittee on transient voltages in arc-furnace circuits: J. E. Hobson, *chairman;* E. R. Whitehead, *secretary;* S. Arnold, E. W, Boehne, F. W. Brooke, L. W. Clark, L. E. Ferri. L. Fountain, C. M. Foust, S. B. Griscom, J. B. Hodtum, W. B. Kouwenhoven, C. C. Levy, W. E. Moore, J. O. Shimmin, N. R. Stansel, *past chairman;* F. J. Vogel, H. H. Wagner.

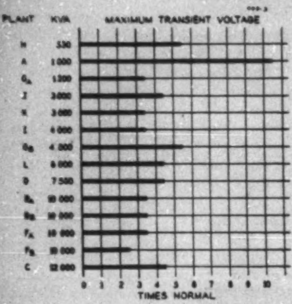


Figure 3. Maximum transient voltage (times normal voltage) plotted against furnace-transformer size

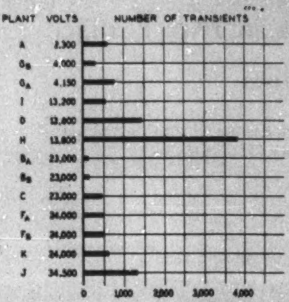


Figure 4. Total number of transients per 1,000 hours of furnace operation plotted against furnace-transformer primary voltage

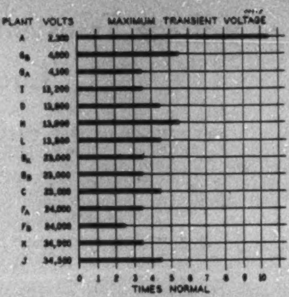


Figure 5. Maximum transient voltage (in times normal) plotted against furnace-transformer primary voltage

in a few cases it was connected line to line.

The transients recorded are segregated into groups according to magnitude. These groups are one to two times normal voltage, two to three times normal voltage, and so on, up to the highest group of 10 to 11 times normal. In all cases unless otherwise stated, this is crest voltage line-to-ground. The actual magnitude of normal crest voltage line-to-ground is given at each installation for convenient reference.

The transient-voltage distribution gives the number of transients per 1,000 hours operation in each class. For instance, plant *A*, test 1, had a total of 574 transients per 1,000 hours operation. The actual test covered a period of 768 hours. There were 30 transients between normal and twice normal voltage per 1,000 hours operation. In the top classification there were five transients per 1,000 hours operation in the 10 to 11 times normal-voltage class.

It was not possible to segregate the transient-voltage distribution for all tests; for instance, in test 2 at plant *A* it is known only that there were 77 transients between one and four times normal per 1,000 hours operation. Similarly, at installation *D* (tests 1, 2, and 3) it was not possible to give a complete breakdown. The tests at installation *E* were made with a vacuum-tube counter, so that the only information available is the number of transients in excess of 20 kv. For example, in test *E*—1, there were 33 transients per 1,000 hours of operation in excess of 20 kv, which is about twice normal voltage.

Test *E*—1 shows the relationship between number and magnitude of transients and breaker operations. The recording device was connected in such a way that it recorded transients only at the time the transformer breaker was closed or opened, and in this particular case 30 of 33 transients occurred at the time of closing the breaker, and only three occurred at the time of opening the breaker. There may have been other transients during normal operation of the furnace, but the equipment was not connected in such a way as to record them.

Test *E*—4 gives all of the transients during normal operation of the furnace, but an arcing ground developed in the cable feed to the transformer during the test, and it is believed that this caused the abnormal number of transients (1,660

Table I. Installation Data Regarding the Furnace Transformer, Circuit Breaker, Grounding, and Insulation of the Secondary Bus at Locations Where Tests Were Made

	Furnace Transformer					Voltage		Circuit Breaker				Grounding			Secondary Insulation		
Plant and Furnace Designation	Kilovolt-Amperes	Type	Rise (Degrees Centigrade)	Frequency (Cycles)	Phases	Primary Volts Delta	Secondary Volts Delta	Oil or Air	Amperes	Kilovolts	Interrupting Kilovolt-Amperes	Transformer Tank	Furnace Shell	Secondary Lamps	Spacing	Creepage	Material
A	1,000	OIWC*	55	60	3	2,300	230	Oil				Yes	Yes	Yes	Busses not interlaced		
Ba	10,000	OIWC	55	60	3	23,000	300	Oil	600	34.5	500,000	Yes	Yes	Yes	Busses interlaced		
Bb	10,000	OIWC	55	60	3	23,000	300	Oil	600	34.5	500,000	Yes	Yes	Yes	Busses interlaced		
C	12,000	OIWC	55	60	3	23,000	270	Oil	600	23		Yes	Yes	Yes	Busses interlaced		
D	7,500	OIWC	40	60	3	13,800	234	Air	600	15	250,000	Yes	Yes	Yes	Busses interlaced		
E	10,000	OIWC	55	60	3	11,000		Air	600	15	250,000	Yes	Yes	Yes	Busses interlaced		
Fa	10,000	OIWC	45	60	3	24,000	290	Oil	600	23	100,000 at 15 kv	Yes	Yes	Yes	1/2 inch	9/16 inch	Micarta
Fb	10,000	OIWC	55	60	3	24,000	240	Oil	600	23	250,000 at 15 kv	Yes	Yes	Yes	1/2 inch	9/16 inch	Micarta
Ga	1,200	OIWC	55	25	3	4,150	225	Oil				Yes		Yes			
Gb	4,000	OIWC	55	25	3	4,000	235	Oil				Yes		Yes			
H	530	OIWC	55	60	1	13,800	125	Oil		15		Yes	No	Yes			
I	4,000	OIWC	55	60	3	13,200	225	Air	600	15	250,000	Yes	Yes	Yes			
J	3,000	OIWC	55	60	3	34,500	220	Oil	600	46	500,000	Yes	Yes	Yes	3/4 inch	3/4 inch	Bakelite
K	3,000	OIWC	55	60	3	24,000	235	Oil	600	34.5	500,000	Yes	Yes	Yes	3/4 inch	3/4 inch	Bakelite
L	6,000	OIWC	55	60	3	13,800	265	Oil		34	500,000	Yes		Yes			

* Oil-immersed water-cooled.

Table II. Power-System Data

Refer to Figure 1 for Key Diagram

Plant and Furnace Designation	Neutral Grounding Method	Transmission Data						Insulation Failures			Remarks
		B		C		D					
		Overhead	Underground	Overhead	Underground	Overhead	Underground	A	B	C	
A		40 feet	No	4,400 feet	No	No	No	No	No	No	Single circuit
Ba								Yes	No	No	
Bb								Yes	No	No	
C		No	90 feet	840 feet	390 feet	6,200 feet	3,200 feet	Yes	Yes	No	Single circuit—arresters at both ends of *C* 500,000 kva short circuit at *B*
D	Ungrounded	40 feet	60 feet	1,000 feet	No	No	No	Yes	Yes	No	Single circuit—184,500-kva short circuit at *B* 3—15 kva, 460-volt capacitors at *A* 3—1/4 microfarads 13.8-kv capacitors and three type *S V* 13.8-kv arresters at *B*
E	Ungrounded delta	No	No	No	705 feet	No	2.76 miles	No	Yes	Yes	Three parallel circuits in *C* 12-kv Westinghouse distribution arresters at *C* 198,500-kva short circuit at *B*
Fa	Solid ground	80 feet	No	1,300 feet	1,300 feet	1,125 feet	1,150 feet	Yes	No	No	Single circuit 850,000-kva short circuit at *B* Westinghouse type *L V* 500-volt
Fb											Arresters at *A* General Electric 24-kv—oxide-film arresters at *C*
Ga	Solid ground	50 feet	No	No	1,500 feet	No	3,000 feet				Single circuit 100,000-kva short circuit at *B*
Gb											No arresters
H	8 ohms resistance	No	400 feet	2.4 miles	No	12.4 miles	2.9 miles	Yes	No	No	Single circuit 15-kv line-type arresters at *C* 120,000-kva short circuit at *B*
I	2.5 ohms resistance	No	100 feet	No	1,700 feet	No	25				Single circuit—no arresters 131,500-kva short circuit at *B*
J	Petersen-coil tests 1, 2, 4; solid ground test 3			4 miles	650 feet	350 feet	No	No	No	Yes	Oxide film arrester at *C* Underground cable at *C* bypassed by overhead for test 4
K	Solid ground	No	No	4 miles	No	No	No	No	No	No	25-kv "porous block" autovalve arrester at *C*
L	2 ohms resistance	No	0.25 mile	3 miles	16 miles	11 miles	165 feet	No	No	Yes	*B*-3 circuits, 2 cables each *C*-4 circuits, 2 cables each 26-kv deion tubes on one phase at *C* 550,000-kva short circuft at *B*

transients per 1,000 hours). There is a somewhat similar comparison between tests *E*—2 and *E*—3, except that it should be noted that the recording device on these tests was on the line side of the breaker. During test *E*—2, there were 11 transients (per 1,000 hours of operation) above twice normal during normal operation; when the recorder (test *E*—3) was connected to record transients only at the time of closing or opening the breaker, no transients were recorded.

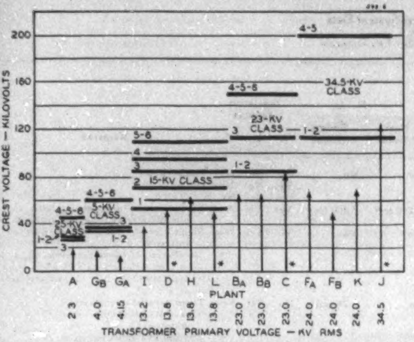
Tests at the *J* and *K* installations also give some information as to the effect of switch openings and closings on the transients. For instance, test *J*—1 shows 1,316 transients per 1,000 hours of operation, and during the test of 250 hours there were 700 switch operations. This indicates a total of 2,800 switch operations for the 1,000-hour period, and 47 per cent of the switch operations could have caused transients. However, test *J*—2 recorded transients only at the time of staged switching operations; and for 672 staged operations there were 315 transients which is also 47 per cent of the operations. This is at least an indication that most of the transients on the transformer side of the primary breaker at this installation occurred at the time of a switch operation.

A similar comparison for tests 1 and 2 at location *K* shows that under normal operating conditions there were transients for 30 per cent of the switch operations and that for the staged switching operations there were transients recorded on 34 per cent of the switching operations. These tests seem very definitely to link the number of primary transients recorded to the number of switch operations.

Under column headed "Test Conditions," an indication is given as to whether the test was conducted under normal operating conditions or under some special condition with perhaps some remedial measures having been taken or some form of corrective equipment added. Tests 1 to 8 at location *D* show the effect of various combinations of arresters, capacitors, and reactors. For instance, with no remedial measures there were 1,460 transients per 1,000 hours operation, and with various combinations of corrective measures both the number of transients and their magnitude were reduced in varying proportions, depending upon the measures used. Similarly, at two tests, *J*—2 and *J*—3, Petersen-coil grounding was replaced with solid grounding, showing both a slight reduction in the number of transients and a slight reduction in the magnitude of the transients. However, the greatest reduction occurred when a 650-foot length of cable in the primary feed to the installation was replaced with overhead, as shown in test *J*—4. This indicates that the electrical characteristics of the circuit have a definite effect on the magnitude and number of transients.

There are only two installations at which transient measurements were made on the secondary circuit of the furnace transformer. These data are summarized in Table V. At location *F* measurements were made on two 10,000-kva furnaces and at location *H* on a single-phase 530-kva furnace. The number of hours operation for the test at location *H* is unknown, so the number of transients could not be reduced to a 1,000-hour operation basis.

The secondary transients are divided into classifications reading directly in kilovolts rather than times normal. It is



Maximum transient voltage crest of 60-cycle one-minute test:

1—Apparatus bushings, small
2—Apparatus bushings, large
3—Air switches and bus supports

Impulse 1.5x40 full wave

4—Apparatus bushings, small
5—Apparatus bushings, large
6—Air switches and bus supports

* Insulation failures occurred

Figure 6. Maximum transient voltage appearing on primary winding of the furnace transformer compared with withstand test voltages of insulation for indoor equipment

thought that there would be little significance in classifying them on a times-normal basis where the actual secondary voltages vary only between 125 and 300 volts. The first column, for instance, in the transient-voltage distribution are those transients between 1 and 2 kv, 2 and 3 kv, and so forth, on up to the final column for the class 12 to 14 kv. Again the number of transients is given per 1,000 hours of operation.

Test *Fa*—3 covers 250 hours operation with the recorder located line to ground and with secondary arresters connected to the bus. There were no transients recorded during this period. Similarly, test *Fa*—6 gives a 720-hour run on the same furnace one year later with secondary arresters, and again no transients were recorded. However, test *Fb*—4 gives the number of transients recorded on the secondary winding of another transformer of practically duplicate rating and fed over practically a duplicate circuit. The only difference is that there were no secondary arresters in the circuit. This test showed 86 transients per 1,000 hours operation with one transient as high as eight or ten-kilovolt crest voltage line-to-ground. Test *Fb*—5 is another test run on the same furnace under similar conditions but with secondary arresters added. This shows 72 transients, but the magnitude has been reduced, and the great majority of transients are in the one- to two-kilovolt class, none higher than the three- to four-kilovolt class, indicating considerable improvement due to the arresters.

About one year later a similar test run was made on the same furnace with the transient recorders connected line to line instead of line to ground, and a vacuum-tube recording device was added to indicate arrester operation. Magnetic links were placed in the arrester circuits to give a measure of the transient current passed by the arresters. The test was conducted for 720 hours, and no transients were re-

Table III. Summary of Test Data on Primary-Voltage Transients

Plant and Furnace Designation and Test Number	Recording Device 1, 2, or 3*	Recorder Location (Line-to-Ground)	Test Operating (Hours)	Furnace Size (Kilovolt-Amperes)	Normal Line Voltage (Rms)	Normal Line-to-Ground Crest Voltage	Total Transients Per 1,000 Hours	Transient-Voltage Distribution—Number of Transients Per 1,000 Hours Operation—Times Normal Line-to-Ground Crest Voltage: 1–2	2–3	3–4	4–5	5–6	6–7	7–8	8–9	9–10	10–11
A—1	1	*B*	768	1,000	2,300	1,870	574	30	305	126	48	30	21	5	3	1	5
A—2	1	*B*	456	1,000	2,300	1,870	77	←	77	→							
Ba—1	1	*B*	1,000	10,000	23,000	18,700	81	7	64	10							
Bb—2	1	*B*	1,000	10,000	23,000	18,700	78	15	62	1							
C	1	*B*	840	12,000	23,000	18,700	420	118	161	36	4						
D—1	1	*B*	150	7,500	13,800	11,200	1,460	←	1,453 →	0	7						
D—2	1	*B*	48	7,500	13,800	11,200	417	←	417 →								
D—3	1	*B*	912	7,500	13,800	11,200	1,170	←	1,169	→	1						
D—4	1	*B*	3,960	7,500	13,800	11,200	540	475	56	9							
D—5	1	*B*	418	7,500	13,800	11,200	42	37	5								
D—6	1	*B*	1,056	7,500	13,800	11,200	125	117	8								
D—7	1	*B*	1,416	7,500	13,800	11,200	208	136	68	4							
D—8	1	*B*	696	7,500	13,800	11,200	292	236	56								
E—1	3	*B*	336	10,000	11,000	8,950	33		←		33	(30 out of 33 were on closing breaker)					
E—2	3	*C*	551	10,000	11,000	8,950	11		←		11						
E—3	3	*C*	336	10,000	11,000	8,950	0										
E—4	3	*B*	240	10,000	11,000	8,950	1,660		←	1,660							
E—5	3	*C*		10,000	11,000	8,950			←	1	(Staged test—automatic trip in single-phase short circuit)						
Fa—1	1	*B*	2,711	10,000	24,000	19,500	500	475	24	1							
Fb—2	1	*B*	713	10,000	24,000	19,500	478	465	13								
Ga—1	1	*B***	84	1,200	4,150	5,850**	107	72	35								
Ga—2	1	*B*	84	1,200	4,150	3,380	798	179	584	35							
Gb—4	1	*B***	168	4,000	4,000	5,640**	209	143	48	18							
Gb—5	1	*B*	250	4,000	4,000	3,250	292	108	124	44	12	4					
H—1	1	*B*	44	530	13,800	11,200	3,810	1,630	1,520	523	114	23					
H—2	1	*B***	44	530	13,800	19,500**	2,412	1,239	955	227							
I—1	1	*B*	72	4,000	13,200	10,800	528	472	55	1							
J—1	1	*B*	250	3,000	34,500	28,000	1,326	784	280	196	56						
J—2	1	*B*		3,000	34,500	28,000		188	67	47	13***						
J—3	1	*B*		3,000	34,500	28,000		60	23	6***							
J—4	1	*B*		3,000	34,500	28,000		25	8***								
K—1	1 and 2	*B*	150	3,000	24,000	19,500	600	480	100	20							
K—2	1 and 2	*B*		3,000	24,000	19,500		168	70	7***							
L—1	2	*B*		6,000	3,800	11,200		15	3	3	1***						

* 1. Lichtenberg figure. 2. Cathode-ray oscillograph. 3. Vacuum-tube counter. ** Line to line.

*** Actual number of transients—not per 1,000 hours of operation.

corded. As far as could be determined, there was no change in furnace operation from the test of a year previous when a large number of transients was experienced; the only known difference was that the recorders were connected line to line instead of line to ground.

Table IV. Summary of Test Conditions

Plant and Furnace Designation and Test Number	Test Conditions
A—1	No remedial measures
A—2	High-voltage capacitor at *B*
Ba—1	No remedial measures
Bb—2	No remedial measures
C	No remedial measures
D—1	No remedial measures, no capacitors, no arresters
D—2	High-voltage arresters, no capacitors, 5.9 per cent reactor
D—3	High-voltage arresters, low-voltage capacitors, no reactor
D—4	High-voltage arresters, low-voltage capacitors, 5.9 per cent reactor
D—5	High-voltage arresters, high-voltage capacitors, 5.9 per cent reactor
D—6	High-voltage arresters, high-voltage and low-voltage capacitors, 5.9 per cent reactors
D—7	High-voltage arresters, high-voltage and low-voltage capacitors, no reactor
D—8	High-voltage arresters, high-voltage capacitors, no reactor
E—1	Transients in excess of 20 kv recorded; transients only at time of opening or closing breaker
E—2	Transients in excess of 20 kv recorded; normal operation all transients
E—3	Transients in excess of 20 kv recorded; transients only at time of closing or opening breaker
E—4	Transients in excess of 20 kv recorded; transients evidently caused by arcing ground in cable at *C*
E—5	Transients in excess of 20 kv recorded
Fa—1	Normal conditions
Fb—2	Normal conditions
Ga—1	Normal conditions
Ga—2	Normal conditions
Gb—4	Normal conditions
Gb—5	Normal conditions
H—1	Normal conditions
H—2	Normal conditions
I—1	Normal conditions
J—1	Normal conditions (700 switch operations); Petersen-coil grounding
J—2	672 staged switching operations; Petersen-coil grounding
J—3	206 staged switching operations; solid grounding
J—4	328 staged switching operations; Petersen-coil grounding; 650 feet of cable replaced with overhead
K—1	Normal conditions (300 switch operations)
K—2	700 staged switching operations
L—1	31 staged switching operations, opening magnetizing current

There is nothing brought out by the secondary tests on these installations to indicate that there is any connection between secondary transients and primary-breaker operations. This installation had four cases of secondary-bus flashovers, all of which occurred during the normal heat run of the furnace at a time when the primary breaker was closed and the furnace was in normal operation, indicating that some of the transients at least are not associated with breaker operation.

Figures 2 through 5 are presented to give a visual indication of the possible existence of any uniform pattern or relationship among furnace size, primary voltage, and the number and magnitude of transients. In all cases the data used are those of tests made when no remedial measures were taken and no corrective equipment added. These charts show no trends nor any functional relationship existing between the previously listed variables. This tends to strengthen the previously expressed thought that electric-system characteristics are a predominant factor in the number and magnitude of transients.

Figure 6 shows the magnitude, in kilovolts, of the maximum transient measured for each test and its relation to the withstand test voltage of insulation for indoor equipment for each voltage class. The AIEE Standards for apparatus bushings of both large and small apparatus, and for air switches and bus supports specify certain withstand test voltages which are a measure of the basic insulation level for each voltage class of insulation. There are some inconsistencies in the values specified in the Standards for the different types of apparatus, but, in general, an insulation level can be indicated whose lower bound is the crest of the 60-cycle one-minute withstand test voltage, and whose upper bound is the impulse withstand test voltage. It is believed that the actual insulation strength against furnace transients of this type, although certainly variable, falls somewhere between these limits.

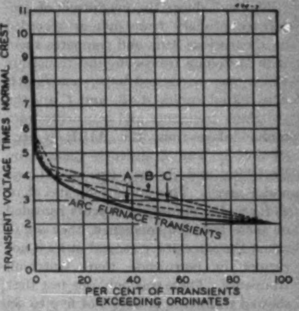


Figure 7. The distribution and magnitude of arc-furnace primary-voltage transients compared with switching and fault transient voltages

A and *C*—Switching transients

B—Transients from faults

From reference 4

At the installations where insulation failures were recorded (plants *D*, *L*, *C*, and *J*), the maximum transients all approach or pierce the lower bound of the insulation level shown on the chart. In this connection it should be pointed out that complete information is lacking regarding the actual insulation levels existing at each test installation, and that, if materially lower levels are encountered, infrequent failures might be expected. Insulation levels recommended for general use in power installations also appear to be satisfactory for most furnace installations. For some devices there are "high" and "low" levels within the nominal voltage rating, and the "high" level should be selected (particularly when the operating voltage approaches the "voltage class"), or it may be desirable to use apparatus of a higher actual insulation level or to apply suitable protection equipment.

Table V. Summary of Transient Voltages Recorded on Secondary Circuits

Plant and Furnace Designation and Test Number	Recording Device 1, 2, or 3*	Recorder Location Line-to-Ground	Test Operating (Hours)	Total Transients Per 1,000 Hours	Transient-Voltage Distribution—Number of Transients Per 1,000 Hours Operation—Crest Voltage in Kilovolts 1-2	2-3	3-4	4-5	5-6	6-7	7-8	8-10	10-12	12-14	Test Conditions
Fa—3	1	*A*	250	0											With secondary arresters
Fb—4	1	*A*	1,028	86	40	26	13	4	0	1	1	1			With secondary arresters
Fb—5	1	*A*	530	72	64	2	6								With secondary arresters
Fa—6	1	*A***	720	0											With secondary arresters
Fb—7	1	*A***	720	0											With secondary arresters
H—3	1	*A*			1	2	2	4	5	6	10***	10	2	1	Normal operation

* 1. Lichtenberg figure. 2. Cathode-ray oscillograph. 3. Vacuum-tube counter. ** Line to line.

*** Actual number of transients—not per 1,000 hours of operation.

Figure 7 shows the magnitude of arc-furnace primary transients as recorded on the various tests and compares them with switching transients and transients which may be caused by system faults. The dotted curves *A*, *B*, and *C* are the switching transients and transients from faults taken from the AIEE paper,[5] "Power-System Transients Caused by Switching and Faults" (see also Lewis and Foust[6]). This curve indicates that, as far as the primary transients recorded on these tests are concerned, they are of about the same order as, and at least not greater in magnitude than switching transients. There was only one test that showed transients in excess of five to six times normal. Of all the transients recorded above twice normal, 92 per cent were less than four times normal voltage. This curve sheet does not bring out the known fact, however, that the frequency of occurrence of furnace transients can be many thousands of times greater than that of ordinary power-system transients.

Summary

1. Voltage transients encountered in primary circuits of furnace transformers have characteristics similar to those of ordinary power-system voltage transients, except that of frequency of occurrence. Primary-circuit transient overvoltages in arc-furnace installations occur during the melting-down operation and at the time of opening or closing the furnace breaker. The highest magnitudes appear to be associated with breaker opening operations. Accordingly, there are more transients in furnace transformer circuits over a given period of time, because furnace breakers operate 40 to 50 times per day compared to a few times a year for the general use of circuit breakers.

2. Insulation levels recommended for general use in power installations also appear to be satisfactory for most furnace installations. For some devices, however, there are wide variations of the insulation level for the same voltage rating, and in those cases where the operating voltage approaches the "voltage class," it may be desirable to use apparatus of a higher actual insulation level as compared to the rating or to apply suitable protective equipment such as capacitors and lightning arresters.

3. There does not appear to be any functional relationship between either furnace size or primary voltage and the number and magnitude of transients on a times normal basis.

4. The magnitude of secondary transients suggests that they originate from current discontinuities in the secondary circuit.

5. This report does not cover internal transformer voltages.

6. Although this report is primarily a factual presentation of test data on voltage transients, some members of the committee thought it desirable to stress the use and adequate maintenance of a mechanically rugged circuit breaker especially selected for arc-furnace service.

Recommendations for Future Analysis and Study

The magnitude of furnace transients, both primary and secondary, appears to be very closely associated with the characteristics of the electric system, and it may prove worth while to investigate this relationship by future calculation and theoretical analysis. It is believed that sufficient power-system data have been tabulated along with the test data to permit such a study by an analysis group.

Wherever the point has been checked, the addition of high-voltage capacitors has been found to reduce both the number and magnitude of the transients recorded, and this fact may support the view that system characteristics are an important factor.

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A New Carrier Relaying System

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IT has long been realized that the surest and most effective way to obtain simultaneous breaker operations at both ends of a transmission line, regardless of the location of a fault within the line section, is by use of some form of pilot channel relaying. Two forms of this type of protection have been developed and used very successfully for a number of years. One system is the familiar distance-type carrier relay system, and the other is the single-element differential pilot-wire system.

This paper describes a new one-to-three-cycle carrier relaying system which combines the simplicity of pilot-wire protection with the versatility of power-line carrier.

A comparison of the characteristics of the distance-type carrier and pilot-wire relaying systems gives the background and reveals the impelling motives for the development of this new system. The distance type of relaying requires a source of current and potential to determine the location of a fault. Backup protection is provided, and auxiliary elements are required to prevent tripping on an out-of-step condition. Consequently, the contact and circuit complication has been increased over that required for a basic carrier relaying system.

In the single-element pilot-wire system, which has been in use for a number of years, certain problems were solved which resulted in definite steps toward simplicity. The chief progress toward simplicity in this scheme was effected by the use of relays operating on symmetrical components of current. This obviated the necessity of using separate relays in each phase and reduced the relay requirements to one per terminal to handle all phase and ground faults. This same symmetrical-component device also reduced the number of pilot wires to two.

No potential source is required for this pilot-wire system, since the positive- and zero-sequence components of the fault current at each end of the protected lines are compared.[1] System out-of-step conditions do not tend to trip the relays and, therefore, do not require auxiliary elements to prevent tripping. The resistance of the pilot wire is one limiting condi-

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tion, however, for this relay. Furthermore, in some cases induction in the pilot wire and differences of ground potential cause undesirable effects.[5]

It is evident from the foregoing that a marked simplification in a carrier relaying system is possible by using the elements of the pilot-wire relay and substituting a power-line carrier channel for the pilot wire. A carrier relaying system of such a nature has been developed, and extensive tests on an artificial transmission line in the laboratory and on an actual line in the field have proved it to be dependable in operation.

Principle of Operation

Basically the relays determine whether a fault is inside a protected line section or external to it by comparing, over the carrier channel, the relative directions of the currents at the ends of the line section. This comparison takes place in the grid circuit of a vacuum tube connected as shown in Figure 1. The tube is normally biased to cutoff; that is, the bias voltage E_c is sufficient to block the flow of plate current when no other voltage is present in the grid circuit. Carrier is transmitted from both ends of the line on alternate half-cycles of the power frequency. A voltage proportional to the received carrier is introduced into the grid circuit of the vacuum tube in series with a single-phase voltage derived from the local line currents. The polarities of these two voltages are such that for an internal fault current flows through the vacuum-tube plate circuit on alternate half-cycles of the power frequency. A voltage is thus induced into the secondary winding of transformer TR which is sufficient to operate the relay element R and close the trip circuit. For an external fault, the phase of one of the voltages in the grid circuit reverses, and the flow of plate current is continuously blocked. Therefore, no voltage is induced in the transformer secondary winding, and the relay does not operate. This vacuum tube is appropriately called a "relay tube" because of its function, and it will be referred to as such hereinafter.

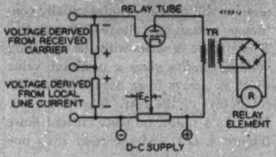

RELAY TUBE
VOLTAGE DERIVED FROM RECEIVED CARRIER
VOLTAGE DERIVED FROM LOCAL LINE CURRENT
TR
E_c
R
RELAY ELEMENT
D-C SUPPLY

Figure 1. Relay tube circuit in which local and distant currents are compared

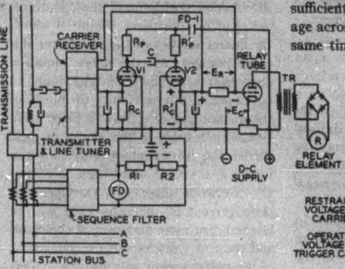

TRANSMISSION LINE
CARRIER RECEIVER
FD-1
R_p
R_p'
C
V1
V2
E_R
RELAY TUBE
TR
R_c
R_c'
E_c
TRANSMITTER & LINE TUNER
R1
R2
FD
D-C SUPPLY
RELAY ELEMENT
R
SEQUENCE FILTER
A
B
C
STATION BUS

Figure 2 (left). Complete basic diagram of simplified carrier relaying system

Complete System

Figure 2 shows a complete basic diagram of one terminal of the relaying system. The three-phase line currents energize the positive-plus-zero-sequence filter. The single-phase output of the sequence filter is connected to a fault detector and to the grids of two thyratrons $V1$ and $V2$. These thyratrons and the associated resistors and capacitors comprise a trigger circuit which is operated by the filter output voltage. The fault detector is used to obtain an intermittent carrier scheme, so that the carrier channel is available for any auxiliary function such as communication or telemetering except during the time of a fault.[4] One contact on the fault detector (*FD*-1) starts carrier through the thyratron trigger circuit, and another contact (not shown) is used to interrupt any auxiliary function.

Upon the occurrence of a fault, the contact of the fault detector closes and applies positive direct voltage to the plate circuits of $V1$ and $V2$. When the grid of $V1$ becomes positive, as a result of the filter output voltage, it fires and its plate or anode current very rapidly rises to a steady-state value as determined by the supply voltage and the plate and cathode resistors (R_p and R_c) of the tube. On the next half-cycle, the grid of $V2$ becomes positive, and thyratron $V2$ fires. When this occurs, the charge on the capacitor C, connected between the plates of $V1$ and $V2$, makes the plate potential of $V1$ momentarily negative which extinguishes $V1$. Thus the wave form of plate current of either thyratron will be square half-cycles of fixed magnitude for any value or wave form of grid voltage, providing, of course, sufficient grid voltage is present to fire the thyratrons $V1$ and $V2$. The voltage drop across the cathode resistor R_c is used as the plate-supply voltage for the oscillator tube in the carrier transmitter. Applying this voltage to the plate of the oscillator causes the transmission of carrier on alternate half-cycles of the power frequency.

The voltage drop across the other cathode resistor R_c' of thyratron $V2$ is connected in the grid circuit of the relay tube in series with the d-c output of the carrier receiver (E_R) as shown. When $V2$ fires, the voltage drop across R_c' makes the grid of the relay tube positive. The d-c output of the carrier receiver is connected with the opposite polarity and is sufficient to cancel the effect of the voltage across R_c' if carrier is received at the same time that $V2$ is conducting. The remainder of the relay-tube circuit is the same as shown in Figure 1. The carrier transmitter and receiver are conventional carrier relaying units whose construction and principle of operation are well known.

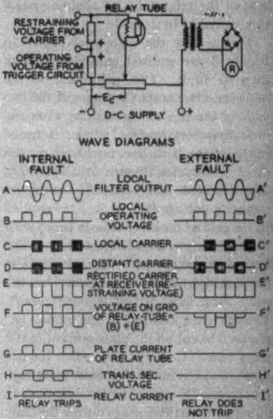

RELAY TUBE
RESTRAINING VOLTAGE FROM CARRIER
OPERATING VOLTAGE FROM TRIGGER CIRCUIT
E_c
D-C SUPPLY
R
WAVE DIAGRAMS
INTERNAL FAULT
EXTERNAL FAULT
A
LOCAL FILTER OUTPUT
A'
B
LOCAL OPERATING VOLTAGE
B'
C
LOCAL CARRIER
C'
D
DISTANT CARRIER
D'
E
RECTIFIED CARRIER AT RECEIVER (RESTRAINING VOLTAGE)
E'
F
VOLTAGE ON GRID OF RELAY TUBE = (B) + (E)
F'
G
PLATE CURRENT OF RELAY TUBE
G'
H
TRANS. SEC. VOLTAGE
H'
I
RELAY CURRENT
I'
RELAY TRIPS
RELAY DOES NOT TRIP

Figure 3. Wave diagrams of relaying quantities

Response to Faults

Figure 3 shows in detail how the circuits just described determine whether an internal or external fault exists. The relay-tube circuit is repeated at the top of Figure 3 for convenience. The "operating voltage from trigger circuit" is the voltage drop across R_c' of Figure 2. It is called operating voltage, as it makes the grid of the relay tube positive, causing the flow of relay-tube plate current. The

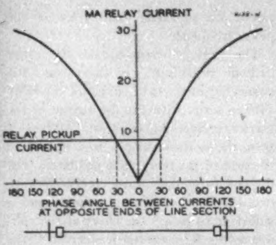


Figure 4. Typical phase-angle–relay-current curve of system

"restraining voltage from carrier" is the d-c output of the carrier receiver (E_R) and is so named as it opposes the operating voltage and prevents the flow of plate current in the relay tube by applying negative potential to its grid.

To see how the quantities shown in Figure 3 are utilized, consider first an internal fault. The polarities of the sequence-filter output voltages at the two ends of the line are such that, when the fault detectors operate, carrier is transmitted from both line terminals on the same half-cycles (Figure 3, *C* and *D*). The received and rectified carrier restraining voltage (Figure 3, *E*) is applied to the grid of the relay tube in series with the operating voltage (Figure 3, *B*). The net voltage on the grid of the relay tube is the sum of these two voltages, and has the wave form indicated at *F* on Figure 3. The reference axis at *F* is the voltage E_c required to cut off the plate current of the relay tube. Consequently, when the net grid voltage is above this axis, plate current flows. For an internal fault, there is no rectified carrier voltage during the half-cycles that the grid of the relay tube is made positive by the operating voltage. As a result, plate current will flow in the relay tube, as shown at *G* in Figure 3. The half-cycle plate-current pulses flow through the primary winding of the transformer *TR*. The secondary voltage (Figure 3, *H*) is rectified to provide a steady relay operating force, and the direct voltage so obtained causes current to flow to operate the relay *R* (Figure 3, *I*).

During an external fault, when the fault detectors pick up, carrier is transmitted from the two ends of the line on alternate half-cycles, as shown at *C′* and *D′* in Figure 3. This change occurs because the current at one end of the line has reversed with respect to the current at the opposite end. If the current entering the line is in phase with the current leaving it, the rectified carrier at each receiver will provide a substantially continuous restraining voltage (Figure 3, *E′*). This voltage in series with the operating voltage (3, *B′*) gives a resultant value which is always more negative than the cutoff bias of the relay tube (Figure 3, *F′*). As a result, no plate current flows (Figure 3, *G′*), and the relay does not operate.

The phase position of the line currents theoretically may fall anywhere between the inphase position and the 180-degree out-of-phase position. Figure 4 shows the characteristics of the relay over this entire range. As the phase position changes from in phase (external fault) to out of phase (internal fault), the blocks of the distant carrier change in phase position with respect to the blocks of local carrier, the two extremes being shown in Figure 3, *C* and *D*, and Figure 3, *C′* and *D′*. Between these two extremes the plate current of the relay tube will be blocked over more and more of the cycle, and the rms value of the a-c component of the plate current will decrease. This will result in a decrease of the current in relay *R* as shown on the typical phase-angle relay-current relationship Figure 4. By adjusting the sensitivity, the relay *R* can be set to operate when the angle between the currents at the two ends of the line becomes any desired value, such as 30 degrees, as shown.

The preceding explanation is based on single-frequency operation of the carrier equipment. That is, the transmitters and receivers at both ends of the line are tuned to the same carrier frequency. It should be noted, however, from a comparison of Figure 3, *B* and *C*, that carrier is transmitted locally only on the half-cycles when there is no operating voltage present. Consequently, it is never necessary to receive the local carrier signal, since the relay-tube plate current always will be zero during these half-cycles. This means that two-frequency operation of the carrier transmitters and receivers is possible without complication, if it is desired to use some auxiliary function which requires a two-frequency channel. In two-frequency operation, the transmitter at one end of the line and the receiver at the opposite end are tuned to one carrier frequency, and the other transmitter and receiver are tuned to a different frequency. This permits simultaneous transmission and reception in both directions as would be needed for telemetering in one direction and load control in the opposite direction. When two-frequency operation is used, the line trap at a given station is tuned to the frequency transmitted from that station.

Figure 5. Type-*HKB* relay used in simplified carrier relaying system described in paper

Special Considerations

Although the settings of the fault detectors at both line terminals usually will be the same, there is the possibility of a distant external fault picking up the fault detector at only one end of the line. As blocking depends on receiving a signal from the opposite end of the line section, carrier must be started at both line terminals before operating voltage is applied to the relay tube at either end.

To do this, the thyratron which starts carrier is set more sensitively than the one which provides operating voltage to the relay tube. The carrier-controlling thyratron also is more sensitive than the fault detector and therefore always will fire whenever the fault detector picks up, but the thyratron which provides operating voltage is set less sensitively and therefore will not fire until the fault current reaches a value approximately 50 per cent above the fault-detector setting. Thus, if the fault current is just above the pickup value of the fault detector, it will pick up, and thyratron *V*1 (Figure 2) will fire and cause the transmission of a continuous carrier signal. The two thyratrons will not trigger back and forth until the fault current is high enough to fire *V*2. Then the system will operate in its normal way. The firing point of *V*2 need be enough above that of *V*1 to insure that the fault detectors at both ends of the line pick up and start carrier before operating voltage is applied to the grid of either relay tube. The difference in

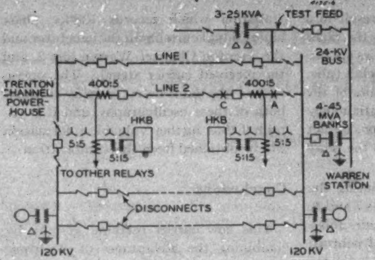


Figure 6 (left). Transmission-system conditions for staged tests

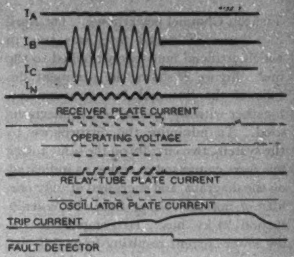
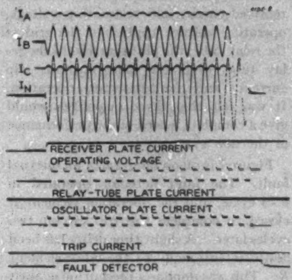


Figure 7 (right). Oscillogram of operation during internal fault

Figure 8 (below). Oscillogram of operation during external fault



settings of $V1$ and $V2$ can be accomplished easily by adjusting the resistor $R1$ in Figure 2 larger than $R2$ to obtain the proper voltage distribution. As the sequence-filter output voltage increases, more of it will be applied to the grid of $V1$ than to that of $V2$, thus allowing $V1$ to fire at a lower fault current than $V2$.

One of the fundamental requirements of the new current-comparison carrier system is that no dependence be placed upon the relative magnitudes of the fault currents at the two ends of the protected line section. In this respect the system operates somewhat differently from the pilot-wire relay in that it is not a ratio-differential scheme but operates only on the relative phase position of the currents at the two ends. The reason why magnitude comparison cannot be used is that to do so would entail the reception of a carrier signal through an internal fault. Also, the attenuation of a power line to carrier frequencies changes with weather and system switching conditions, resulting in the reception of a signal which varies in strength over a wide range. While some signal, no doubt, could be received through an internal fault, especially if high power were used, it seems fairly obvious that no dependence could be placed on the reception of a signal of the proper magnitude. In the present system, therefore, the circuits which control tripping and blocking are saturated at as low a current value as possible, and only the variation in phase position is utilized to discriminate between internal and external faults. Also, it is only necessary to receive a signal from the far end of a line section when blocking is required, that is, during a fault external to the protected section.

The carrier receiver saturates at a small value of received signal and provides sufficient restraining voltage to give a good margin of safety over the operating voltage over a very wide range of carrier attenuation. This is done to insure dependable blocking for external faults. It should be noted in Figure 3, *C*, *D*, and *E*, that the restraining voltage due to the reception of both the local and distant signals is no greater than for the reception of the distant signal alone (Figure 3, *C′*, *D′*, and *E′*). This is due to the saturation of the carrier receiver. As a result of saturating all the quantities involved, a single curve (Figure 4) suffices to show the relay-system characteristic over a wide range of fault currents.

On lines with a source of power at only one end, this system will block for an external fault beyond the line terminal having no source of power. However, for an internal fault, only the end having a source of power will trip. This operation is the same as for a standard distance-type carrier relaying system.

The application of this relaying system to three-terminal lines has been considered, and there appears to be no fundamental reason why it will not operate satisfactorily.[6] However, to date no actual tests on a three-terminal line have been made.

There is a theoretical condition where the use of the positive-plus-zero sequence filter may result in incorrect operation. This occurs on an internal *B-C*-to ground fault when the positive-sequence current all comes from one end of the line and the zero-sequence current all comes from the opposite end. In addition the magnitudes must be exactly right, and the phase angle of the positive- and zero-sequence system impedances must be equal. This same condition exists for the pilot-wire relay which uses the same type of network filter, but in no installation has this exact condition ever been encountered. If it is a condition which must be satisfied, the use of positive sequence alone is a solution.

The Equipment

The relay proper contains the positive-plus-zero sequence filter, the operating element, and the fault detector. Figure 5 is a picture of the relay in a case equipped with switches to facilitate testing. The trigger circuit, relay tube, and associated components are mounted on a 19-inch-wide panel for relay rack mounting in the same cabinet as the carrier transmitter and receiver. Provision is made to check the operation of the circuits without disturbing the installation.

Field Tests

In order to obtain field experience under typical service conditions, the first installation of this carrier relaying system was made in April 1944 on a 120-kv transmission line of The Detroit Edison Company's system. Before being installed in the field, the equipment was set up completely by the company in its laboratory for a preliminary trial. A miniature transmission line, which could be fed independently from either end, was connected to the relays through current transformers, and all types of fault conditions were simulated. These preliminary tests did not disclose any incorrect relay operations during a large number and variety of applied faults. The tests also permitted a very complete study of the effects of all adjustments on the equipment.

After this thorough trial in the laboratory, the equipment was installed on the Trenton Channel–Warren line 2 (see Figure 6) which is one of four parallel circuits feeding the Warren station from the

Trenton Channel powerhouse. On April 2, a series of staged tests was run, in which artificial faults were applied to the line. Figure 6 shows the pertinent details of the line and bus arrangement, together with the special test connections used. To minimize the disturbance to the system, two circuits on the same tower line were connected in series, as indicated on the diagram, and energized at only one fifth of normal voltage from the Warren station 24-kv bus. To compensate for the lower current resulting from this circuit arrangement and to obtain values more representative of actual faults, the secondary current was stepped up three times through a set of auxiliary current transformers before feeding into the relay. The 24-kv system is solidly grounded in the same way as the 120-kv system. The a-c source was connected to the line 1, and the faults were applied to line 2 at the Warren station terminal under three different test conditions. In the first condition, the fault was applied at point *A* (Figure 6) just external to the protected zone. For the second condition, the fault was applied at the same point, but at Warren station the polarity of the sequence-filter output was reversed to simulate an internal fault with a source of power at both ends of the line. For the third condition, the fault was applied at point *C* to simulate an internal fault fed from only one end of the line.

Ten tests were made under each of the conditions outlined, including a solid three-phase fault and all combinations of line-to-line, double-line-to-line-to-ground, and single-line-to-ground faults. In all cases, the test condition was set up with the 120-kv lines de-energized and then the 24-kv breaker was closed to initiate the fault current. In every case, the relays operated correctly. No tripping occurred on any of the external faults, whereas all internal faults were cleared promptly by the action of the proper circuit breakers.

Oscillograms were taken at Warren station of every test and showed essentially consistent and positive operation of all components of the relaying system. The test control sequence was such that oscillographic records of the beginning and the end of the fault-current flow were recorded whether the fault was cleared by the line breaker or by the test feed breaker. This was done so as to include on the oscillograms the effect of switching transients.

The average relaying time was 2.5 cycles with the slowest operation a cycle longer which was probably due to the random initiation of the fault in terms of the 60-cycle wave. This can result in a variation of about one cycle in the point at which the trigger circuit applies operating voltage to the grid of the relay tube. For these tests, the plate circuits of the thyratrons *V*1 and *V*2 were continuously energized, and the fault-detector contact was in the plate-supply lead to the relay tube.

Typical oscillograms taken during the staged tests are shown in Figures 7 and 8. The quantities recorded include relay current in the three phases and neutral, detector plate current which indicates received carrier and restraining voltage, operating voltage applied to the grid of the relay tube, the plate current of the relay tube, oscillator plate current, trip current, and fault-detector operation. It was felt that these quantities would give a complete record of the performance of the new system.

Figure 7 is an oscillogram of an internal fault. The fault detector operated in 0.75 cycle, and the operating element *R* closed to complete the trip circuit two cycles later. A slight time delay has been added intentionally to the relay element *R*. This was done as a precaution against operation on a single pulse of energy caused by a transient during the clearing of an external fault. The tests indicate that the time delay selected was more than enough. Additional tests in the laboratory are being made to determine the amount of time delay, if any, which will be required. Figure 8, which is an oscillogram of an external fault, indicates that the time delay is unnecessary. Reference to the trace of relay-tube plate current in Figure 8 shows that it remained zero during the entire time of fault. The external fault was cleared by automatically opening the 24-kv breaker approximately 18 cycles after it closed. The wave form of the quantities recorded on the two oscillograms is in good agreement with the theoretical values shown in Figure 3. The carrier receiver was adjusted for a high sensitivity to obtain the maximum effect of any transient which might affect the received carrier signal.

The equipment was put into service immediately after the tests, and has performed according to expectations to the date of this writing. Special records on the operation of this equipment are being kept. Indicators and counters provide records of the operation of the fault-detector and the tripping relay. Warren station has an automatic oscillograph in service which records three-phase voltage and neutral current on the 24-kv system. Another has been added for this trial installation which records 120-kv phase and residual currents in the generator and the Trenton Channel–Warren line 2, and the received carrier signal. The operation of the fault-detector relay initiates both of these oscillographs, and it is expected that further valuable information will be obtained from this combination.

Conclusions

This new carrier relaying system, by combining the advantages of electronic tubes and symmetrical-component filters, results in a marked simplification over previous carrier relaying systems. The number of relays required for complete phase and ground protection has been reduced to one per terminal, and the number of moving elements in the relay has been reduced to two.

The system operates on current only, eliminating the need of a source of potential and the attendant requirement of minimum voltage operation. It will not trip during system swings or out-of-step conditions. It is particularly well adapted to installation on circuits where existing relays would provide satisfactory backup protection to a carrier system. For this reason, lower-voltage circuits from 66-kv down appear to present a large number of possible applications for the new relay system.

The use of a fault detector retains the feature of an intermittent carrier system and permits the use of the relaying carrier channel for other services.

The system compares phase positions of current and therefore can be made very sensitive. It is easily applied, and the saturation characteristics make it practically independent of fault-current magnitudes.

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Current-Transformer Output and Application Charts

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Synopsis: The performance of a multiratio current transformer is most simply represented as a function among the admittance vector locus of the core for one turn, the secondary-turn number N, and the secondary impedance Z. If the ratio of N^2/Z is used as a variable, extremely simple application charts for ratio correction factor in relation to primary currents result. The ratio N^2/Z furthermore is basic in the matching of transformers and burdens for maximum volt-ampere output at given primary current. Methods for computing the optimum volt-ampere output are given, and a chart is derived from which all data for a correct matching can be obtained at once. The usefulness of these methods is explained in connection with various examples such as applied to relaying circuits commonly used.

In recent years the theory of operating of multiratio bushing-type current transformers has been given extended study resulting in a simplification of the application curves.[1,2] There is still lacking, however, a single chart from which the ratio correction factor for any value of primary current, any turn number, any secondary burden, and burden power factor can be read directly. In this paper a study is made to determine the feasibility of such a chart.

To date, there is very little information available on the secondary-output variation with relation to primary current, turn numbers, and burden. In many instances the secondary volt-ampere output is the quantity determining the operation of connected apparatus, such as instruments and relays. It therefore appears that benefits would be derived from a formulation of the output characteristics, particularly in the field of low primary currents. Of particular value would be the development of a volt-ampere-output application curve which would enable the relay engineer to determine quickly the optimum performance.

Standard Application Curves

In recent years numerous efforts have been made to simplify the representation of errors in multiratio relaying-type current transformers. A particularly simple graphical solution results for the ratio correction factor and phase-angle error when the admittance-vector locus of the steel core is used.[1] The complex ratio can be represented as a function of the number of secondary turns, the secondary burden and the admittance vector for one turn.

If one designates

N = number of secondary turns
Z = total secondary burden
Y_1 = admittance vector for one turn
$R(1/1)$ = one-to-one ratio

the following relation exists:

$$-R(1/1)=\frac{\dfrac{N^2}{Z}+Y_1}{\dfrac{N^2}{Z}}$$

resulting in the transformer chart shown in Figure 1 for the case of a typical 23-kv specimen. All the following charts will be based on this 23-kv specimen. In most relaying applications knowledge of the phase angle is not required. The small amount of drafting work necessary in conjunction with Figure 1 can be avoided by plotting curves representing various N^2/Z values in a co-ordinate system with ratio correction factor as ordinate and primary current as abscissa.

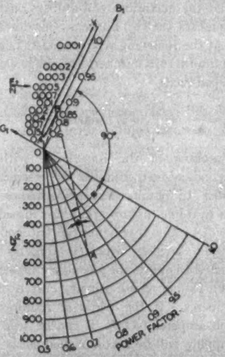

Figure 1. Chart for calculation of ratio and phase angle for any number of secondary turns N, any total burden Z at any power factor, and any desired secondary current I_2, provided $N^2/Z \leq 1{,}000$

Instructions:

1. Compute volts per turn I_2Z/N and mark point on locus Y_1, corresponding to this value as B
2. Compute N^2/Z and mark corresponding point A at proper power factor
3. Phase angle θ = angle OAB
   True ratio $R_N = I_1/I_2 = N \times AB/AO$

N = Number of secondary turns
Z = Total secondary impedance

Figure 2 represents a practical chart giving the required information without necessitating drafting and with the least amount of computations. As has been pointed out in a recent paper,[2] for most transformers the ratio error is not affected greatly by changes in the power factor of the burden. The curve therefore applies for any burden power factor encountered in practice. The use of the chart shall be explained in conjunction with an example:

Assumed total burden = 4.0 ohms (at 50 per cent power factor)
Transformer ratio = 250/5 = 50/1

What is the ratio correction factor for a primary current of 150 amperes?

Calculate

1. $\dfrac{N^2}{Z}=\dfrac{50^2}{4}=625$

2. $I_1\dfrac{Z}{N^2}=\dfrac{150}{625}=0.24$

Select this latter abscissa on the chart, point A, and follow vertically to the curve corresponding to $N^2/Z=625$. If there is no curve drawn for this value, interpolate between two curves including this value, giving point B. The ordinate of point B corresponds to the ratio correction factor (RCF), the value of which can be read on the ordinate axis, point C, giving RCF = 1.11. This chart permits the quick determination of the RCF of a specific transformer for any secondary-turn number, any secondary burden of any usual power factor and at any primary current. It has the advantage of not being limited to the specific burdens usually referred to on similar charts. This seems to be an important feature since burdens encountered in practice never or only accidentally correspond to these specific burdens.

Figure 2, together with the admittance-vector-locus chart shown in Figure 3, appears to satisfy the American Standard C57.1—4.033, covering current-trans-

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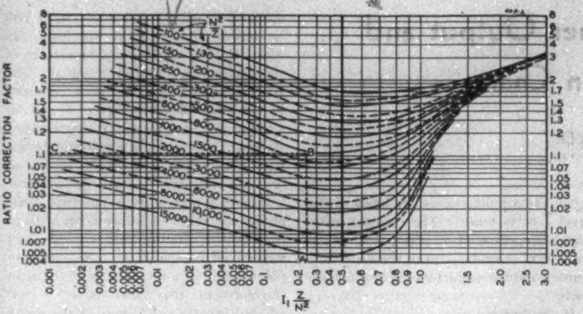

Figure 2. Chart giving ratio-correction factor for any number of secondary turns N and any total secondary impedance Z at power factors between 50 and 100 per cent and for any primary current

Internal $R=0.0022N$ ohm
Internal X considered negligible

former application data completely and in the simplest and still most general form. A chart similar to Figure 2 can be prepared for the representation of the phase-angle errors. Such a chart, however, is sensitive to power-factor changes in the burden and therefore is limited to one specific power factor.

Secondary Volt-Ampere Output

It is the prime purpose of a current transformer to provide an appropriate supply of secondary power for the operation of various devices, such as meters, relays, and trip coils. These devices require a definite amount of power to perform the desired operation. Difficulties are encountered in many cases where operation is required at very low primary current. It is known that for each multiratio transformer there appears to be a definite ratio giving best performance under given primary current and load conditions. In the following it will be shown, that the optimum performance of any multiratio transformer can be determined from a turn-impedance matching chart requiring but a few simple computations. Since this chart is based on an analysis of the secondary volt-ampere output in relation to primary current, turn number, and secondary burden, these factors shall be investigated in detail. It will be seen that the admittance-vector locus offers a simple means of obtaining the desired data.

Variation in Volt-Ampere Output With Varying Secondary-Turn Number, Constant Secondary Impedance, and Constant Primary Current

Figure 4 shows the variation in secondary volt-ampere output for 500-ampere primary current and two secondary burdens of 3.4 ohms and 13.6 ohms, in relation to the secondary-turn number. It can be seen that pronounced maxima exist at 40 and 80 turns respectively. For the 3.4-ohm burden the output at 20 turns is only about one-third the maximum output of 325 volt-amperes occurring at 40 turns. It can be noted further that the maximum volt-ampere output is equal for the two burdens considered and that the following relation exists between optimum turn numbers and the respective impedances:

$$\frac{N^2}{Z}=\frac{40^2}{3.4}=\frac{80^2}{13.6}=471=\text{constant}$$

Matching of the impedance with the secondary-turn number is essential in order to obtain best performance. As an example, assume that the impedance $Z=3.4$ ohms represents the trip coil of a circuit breaker which is directly connected to one of the secondary taps of the transformer whose primary current is 500 amperes. If the trip coil requires 300 volt-amperes to trip, it can be seen that tripping will be accomplished easily with the 40-turn connection but is impossible with any of the other standard connections of the secondary winding.

Variation in Volt-Ampere Output With Varying Secondary Impedance, Constant Turn Number, and Constant Primary Current

Figure 5 shows the variations in volt-ampere output for 500-ampere primary current, for various fixed secondary-turn numbers and varying burden impedance. The maximum volt-ampere output again amounts to 325 volt-amperes. The output is very sensitive to changes in magnitude of the burden impedance but is only little affected by changes in power factor as shown in Figure 5. The optimum performance occurs at the same N^2/Z value of 471 as found in Figure 4. From Figures 4 and 5 it follows that for a given primary current there exists a definite volt-ampere-output maximum. It can be seen also that independent of the secondary-turn number N the maximum occurs at a definite flux density of the core corresponding to $E_2/N=0.83$ volt per turn in case of 500 amperes primary current.

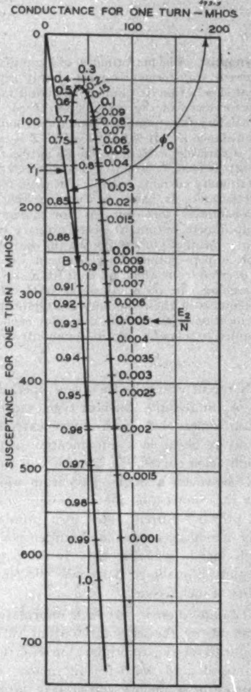

Figure 3. Admittance-vector locus Y_1 in mhos for one turn as a function of volts per turn

Determination of exciting current:

1. Determine volts per turn I_2Z/N giving point B, $Y_1=OB$ and ϕ_0
2. $Y_N=Y_1/N^2$
3. $I_0=Y_NE_2$

Internal reactance considered negligible
Internal resistance, $R=0.0022N$

Variation in Volt-Ampere Output With Varying Primary Current

Increased volt-ampere output requires increased primary current. Figure 6 represents tests on 80 turns with varying primary current and varying secondary impedance. At 1,000 amperes the maximum equals 710 volt-amperes; at 700 amperes 480 volt-amperes; at 500 amperes 325 volt-amperes, and at 300 amperes 175 volt-amperes.

For various primary currents and constant number of turns the maxima do not occur at the same secondary-burden impedances and hence not at the same value of N^2/Z. Therefore, if a transformer has been matched for optimum volt-ampere output at a specific primary current, the secondary impedance and turn numbers must be rematched for another primary current to produce maximum output. The curves shown in Figures 4, 5, and 6 are too involved for use as practical application curves. Figure 7 shows a curve *A* which relates the primary current with the burden impedance *Z* and the turn number *N*, in particular N^2/Z, at which the output maximum occurs. Curve *B* designates the maximum volt-ampere-output corresponding to various primary-current values. The use of this chart will be explained by means of a specific example.

A relay having a burden of six ohms is set to operate at five amperes. What is the minimum primary current value at which tripping takes place and what is the corresponding optimum turn number?

The secondary volt-amperes at tripping are equal to $5^2\times6=150$ volt-amperes. From curve *B*, Figure 7, the corresponding minimum primary current amounts to 275 amperes. If we refer to curve *A*, the corresponding N^2/Z value amounts to 260. Since *Z* is equal to six ohms

$$N^2=6\times260=1{,}560 \text{ giving}$$

$$N=\sqrt{1{,}560}=39.5 \text{ turns}$$

The closest turn number available on standard taps is 40 turns. The relay should be connected to this tap; if the relay were connected to a different tap of the transformer, tripping would take place only at a higher primary current. It is apparent that impedance matching is essential in order to procure best performance.

Graphical Determination of Volt-Ampere Output From Admittance-Vector Locus

The secondary output can be determined readily from the admittance-vector locus, for one turn as will be explained in the following.

Figure 8a shows a graphical representation of the primary, secondary, and exciting ampere-turn relation of a transformer with equal number of primary and secondary turns *N*. For the case of one primary turn and *N* secondary turns the single-turn primary current can be obtained from the relation $I_1'=NI_1$.

The current diagram 8b is obtained from the ampere-turn diagram by dividing the vectors in diagram 8a by the turn number *N*. As a first step it will be indicated how the volt-ampere output can be constructed from this current diagram. This method requires an exciting-current curve in polar form.

For a fixed value of primary current the distance *BC* in Figure 8b remains constant, whereas $OB=I_e$ and $OC=I_2$ vary with the secondary impedance, respectively with corresponding variation in volts per turn. With open circuited secondary the primary current is equal to the exciting current $I_1=I_{e0}=OA$. The procedure of obtaining the volt-ampere output for constant primary current is therefore to assume a secondary-voltage value E_2, determine the exciting current giving point *B* and find the intersection of the circle with center at *B* and radius I_1 with the line *OC* corresponding to the power-factor phase angle of the secondary burden, giving $OC=I_2$ and the output $=I_2E_2$.

However if the vectors in Figure 8b are multiplied by N^2/E_2, the admittance diagram 8c is obtained. Since the admittance-vector locus is available (Figure 3), it is advantageous to use it in the determination of the volt-ampere output. Whereas in Figure 8b, distance *BC* is constant, in Figure 8c it varies with the secondary volts per turn value E_2/N.

The secondary volt-ampere output for a constant primary current $I_1'=NI_1$ and a specific value of E_2/N represented by point *B* can be obtained as follows:

1. Calculate $\dfrac{I_1'}{E_2/N}$

2. Intersect the circle with center *B* and radius $I_1'/(E_2/N)$ with the power-factor line *OD*, producing point *C*.

3. The volt-ampere output at point *B* is equal to $E_2^2/Z=(E_2/N)^2N/Z$ and is obtained by scaling *OC* and multiplying its value by $(E_2/N)^2$ of point *B*. This output is plotted most conveniently in an orthogonal co-ordinate system with *OD* as abscissa, as indicated in Figure 8c.

Figure 8c brings out the important fact that the volt-ampere output is not primarily a function of *Z* or *N*, but of the ratio N^2/Z. This is the basic variable in volt-ampere-output problems as well, and it is for this reason that the impedance matching curves (Figure 7) become simple.

Mathematical Determination of Volt-Ampere Output

Without loss in generality, the volt-ampere output can be computed by assuming a transformer having one primary and one secondary turn. In the equivalent diagram, it can be represented as shown in Figure 9 where Z_T represents the impedance of the transformer and Z_B the secondary-burden impedance. Depending on the value of Z_B, a larger or

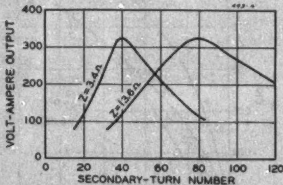

Figure 4. Variation in volt-ampere output with varying secondary-turn number, fixed secondary impedances, and constant primary current

$I_1=500$ amperes
Burden power factor=0.5

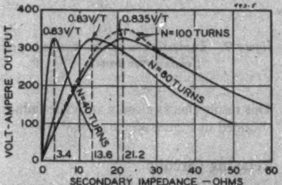

Figure 5. Variation in volt-ampere output with varying secondary impedance, fixed turn numbers, and constant primary current

$I_1=500$ amperes
——50 per cent power factor
- - -90 per cent power factor

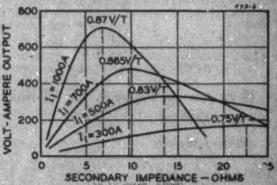

Figure 6. Variation in volt-ampere output with fixed primary currents, varying secondary impedance, and constant secondary-turn number

50 per cent power factor
$N=80$ turns

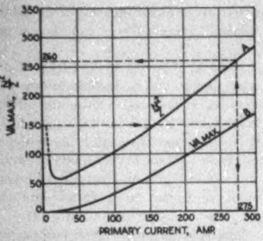

Figure 7. Impedance matching chart giving maximum volt-ampere output and corresponding N^2/Z for a given primary current

smaller portion of the constant total current I_1 is diverted through the secondary winding. $\boldsymbol{Z_T}$ is equal to $1/\boldsymbol{Y_1}$ and is obtained from $\boldsymbol{Y_1}$ by complex inversion, resulting in Figure 10. In case of a highly reactive burden Z_B, a close approximation is obtained by neglecting the resistance in Z_T and Z_B. This simplification is similar to that previously made in the case of inphase addition of currents.

It has been shown further that the susceptance component $\boldsymbol{B_1}$ of Y_1 is proportional to the eighth power of the secondary voltage for high flux densities, and proportional to $E^{-0.4}$ for very low flux densities.[1]

An example covering the high flux-density range will be computed in the following, the primary current being assumed 500 amperes: With the transformer open-circuited, the entire primary current is used to magnetize the core, resulting in

$$\boldsymbol{I_1} = \boldsymbol{Y_{1.0}E_{2.0}} \text{ for } N=1$$

Reference to Figure 3 proPuces $Y_{10} = 514$ mhos, $E_{2,0} = 0.973$ volt. These values can be established by a few trials.

Y_1 for any secondary voltage E can therefore be expressed as

$$Y_1 = 514\left(\frac{E}{0.973}\right)^8$$

and

$$Z_T = \frac{1}{Y_1} = \frac{1}{640E^8}$$

If we refer to Figure 9, the following relations exist:

$$(500-I_2)Z_T = E$$

$$(500-I_2)\frac{1}{640E^8} = E$$

Hence

$$I_2 = 500-640E^9$$

and

$$VA = I_2E = 500E-640E^{10}$$

which derivative with regards to E is to be zero for maximum volt-ampere output.

This results in

$$E_m = \sqrt[9]{\frac{500}{6{,}400}} = 0.754 \text{ volt}$$

Hence

$I_2 = 450$ amperes
and the maximum output $= 339\ VA$

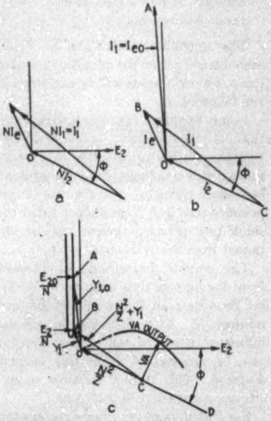

Figure 8. Graphical representation of relations between primary, secondary, and exciting ampere turns, currents, and admittances

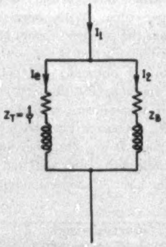

Figure 9. Equivalent circuit for bushing-type current transformer

This result checks closely with the values obtained in Figure 6.

Relaying Circuits

In many of the most common relaying circuits there is usually a large safety factor between the minimum amperes required to actuate the relay and the actual short-circuit current. In these cases little attention has to be paid to impedance matching in order to produce maximum volt-ampere output. In special cases, however, where the short-circuit current is low, where part of the secondary current of the transformer of the short-circuited phase is by-passed through idle transformers, and finally where impedances of relays are high or vary within great limits, a correct matching of the impedances becomes important.

In previous example it has been shown how the combination of a single transformer with a relay can be tuned to produce tripping at minimum primary current. The method outlined determines directly the optimum turn number for a given relay impedance by use of Figure 7. The method can be applied directly for a symmetrical three-phase short-circuit in conjunction with three-phase relays and one ground relay in accordance with Figure 11a. In case of single-phase-to-ground and phase-to-phase faults the method has to be modified because of the current by-passed in the idle transformers of the normal phases. For approximation calculations some simplifying assumptions can be made. The load current in the two normal phases will be neglected, and, whenever permissible, inphase addition of currents shall be applied. Small impedances also can be neglected when in series with large ones. Because of the nonlinearity of the circuits caused by the variation in the impedances in the by-passed transformer as indicated in Figure 10, the optimum performance has to be determined by a step-by-step method. Since no general rules applying to all the numerous relaying circuits can be set up, the application of the methods shall be given in conjunction with a specific example.

In the circuit shown in Figure 11a a

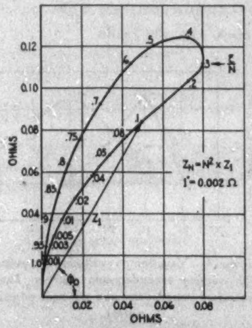

Figure 10. Impedance-vector locus Z_1 for one turn as a function of volts per turn

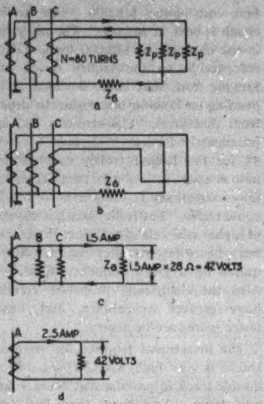

Figure 11. Relaying circuit and simplified representation

combination of ground relays has an impedance of 28 ohms (700 volt-amperes at five amperes) at 60 per cent power factor. The ground relays are actuated at 1.5 amperes.

The phase relays have an impedance of 0.2 ohm (five volt-amperes at five amperes) and 70 per cent power factor. A single-phase-to-ground fault is assumed in phase *A*.

The problem consists in determining:

1. The minimum phase-to-ground current in phase *A* necessary to trip the ground relays.
2. The corresponding optimum secondary-turn number *N* of the current transformers.

In order to calculate the current by-passed by the idle transformers *B* and *C*, the small impedance of the phase relays can be neglected, and the circuit can be simplified according to Figure 11b. Since no rigorous solution is possible, the minimum primary current (which is just necessary to trip the ground relay) corresponding to several transformer ratios has to be determined, the results of which have been plotted in Figure 12. From these values the optimum turn number can be determined. As a first attempt the 400/5 ratio is selected corresponding to 80 turns. As a first step the currents by-passed through the secondary circuits *B* and *C* have to be determined. The admittance of the current transformers *B* and *C* can be obtained from Figure 3. If we refer to the simplified Figure 11c and recall that the ground relay trips at 1.5 amperes, the corresponding volts per turn are $42/80=0.525$. The admittance Y_1 is 77 mhos for one single turn. For 80 turns

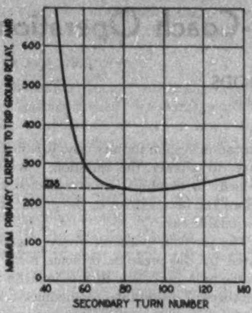

Figure 12. Optimum number of turns for relaying circuit of Figure 11

$$Y_{80}=\frac{Y_1}{80^2}=\frac{77}{6,400}=0.012 \text{ mho}$$

The circuit can be simplified further by combining the impedance of the secondary windings *B* and *C* with that of the ground relay. Since the power factors of relays and current transformers are not very different, the resultant admittance can be obtained by numerical addition.

$$Y=Y_B+Y_C+Y_{\text{relays}}=2\times 0.012+0.0357=0.0597 \text{ mho}$$

corresponding to a resulting impedance of 16.8 ohms. The total secondary current necessary to drive 1.5 amperes through the circuit is therefore

$$I_2=42/16.8=2.50 \text{ amperes.}$$

The problem now resolves itself to determine the primary current necessary to produce 2.50 amperes across a secondary burden of 16.8 ohms at 60 per cent power factor (see Figure 11d). This can be done by means of Figure 1.

The volts per turn are $\frac{42}{80}=0.525$

$$\frac{N^2}{Z}=\frac{6,400}{16.8}=381$$

$$R(1/1)=\frac{AB}{AO}=1.19$$

$$R_{80}=I_1/I_2=80\times 1.19=95.3$$

Therefore

$$I_1=95.3\times 2.50=238 \text{ amperes}$$

If 80 turns are used, tripping can be accomplished for a relay current of 1.5 amperes with a primary current of 238 amperes. By repeating the calculations for other numbers of turns, a curve as shown in Figure 12 is obtained. From this figure it can be seen that the optimum performance occurs for $N=90$ turns.

Conclusions

A simple application chart from which the ratio correction factor can be determined for any value of primary current, any number of secondary turns, and any secondary burden of practical power factor has been developed.

The secondary volt-ampere output of a current transformer at a given primary current varies with the secondary-turn number and secondary burden. In order to obtain maximum volt-amperes at a given primary current, matching of the secondary impedance and turns is required. A volt-ampere-output application curve has been developed which is readily applicable to numerous relaying problems.

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Economics of Trolley-Coach Operation

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Synopsis: The trolley coach is one of the newer vehicles used in urban transportation. In 1943 trolley coaches carried 8.7 per cent of all passengers on the transit lines of all United States cities having a population between 50,000 and 1,000,000, although trolley coaches are only 6.6 per cent of the vehicles. This paper points out the features of trolley-coach operation which fit it for a wide variety of service conditions. Results of trolley-coach operation in increasing gross revenues are given. The various developments which have contributed to low transportation expense are outlined. Comparisons of trolley-coach, streetcar, and motorbus operating expenses are included. Further progress in trolley-coach design, which can be expected to follow the war, will enhance the trolley coach's economic position.

SINCE the introduction of the modern trolley coach in 1928, it has demonstrated in almost every installation its ability to increase revenues and to operate at very low cost. The investment cost for trolley-coach service is low, and the quality of that service ranks very high compared to other transportation media. The vehicle itself and the materials used in the distribution system have been improved continually with resultant improvement in the financial showing. Further progress is under way.

As a result of the success of early installations, there are 3,500 trolley coaches in service in the United States and Canada. The number has been reached in spite of the years of depression and war, both of which have retarded the growth.

In the broad field of urban transportation there is room for many types of public conveyances; rapid-transit trains, streetcars, trolley coaches, motorbusses, and taxicabs. Rapid-transit elevated or subway service is limited to a few large cities, and taxicabs furnish a distinct type of service at a much higher price; therefore both will be omitted from further discussion. But most transit companies, in cities of 100,000 population up, are faced with the possible use of the three remaining types of vehicles. In order to develop the most useful transportation system for any city, it is necessary to answer the question, "Which vehicle or combination of vehicles will afford the best service to the public at a reasonable cost?"

The problem is almost always complicated by the presence of some form of public transportation that is inadequate or otherwise unsuitable. Sometimes it is a streetcar system whose only excuse for existence was the hope of its promoter that he might make an honest dollar. Sometimes it is a motorbus system which grew from unemployment and easy credit in a community which now would be better and more economically served by trolley coach or streetcar.

Although the transportation engineer cannot hope to find virgin territory where each type of vehicle can be fitted into its correct economic niche, the factors determining the selection of the proper vehicle should be recognized. Once the right perspective is obtained, the economic chaos which characterizes too many of our activities can be avoided. Investment, earning power, and cost of operation are the three important factors in determining the correct application.

Investment

The first consideration is the initial cost of an installation. It can be assumed that for equal standards of convenience and excellence shops, car houses, garages, and similar items will be about the same, regardless of the type of vehicle. Of course, the detail arrangements differ, and each form of vehicle requires a maintenance facility or practice peculiar to itself: such as tracks for the streetcar, double overhead wires for the trolley coach, and fueling provisions for the motorbus. Likewise some feature which keeps down the first cost may entail considerable differences in day-to-day expenditures for the several forms of vehicle. For example, with outdoor storage in cold climates electric vehicles may require preheating in varying amounts proportional to their size, and motorbusses need engine idling to prevent freezing and insure starting in the morning. Such things must be evaluated in determining the initial investment for maintenance facilities.

The modern streetcar has the highest first cost, some $18,000. The trolley coach is next at $13,000. The motorbus is the cheapest at $12,000 for a vehicle comparable to the trolley coach and ranging from that value downward, depending on how far one wishes to depart from that level. The streetcar has approximately 58 seats compared to 40 to 44 for the largest trolley coaches and motorbusses in common use; therefore fewer streetcars are required to handle equal traffic. The trolley coach is capable of higher schedule speeds than the motorbus; hence fewer trolley coaches are required for a given frequency of service. Also the electrically propelled vehicles have greater availability, and hence fewer spares are necessary.

The investment required for streetcar tracks is very high, $100,000 per mile of double track in paved street being a normal figure. As a result there has been little completely new track built in many years. When new track is required on a route, the advisability of replacing it or substituting some form of rubber-tired service must be studied. The trolley coach requires overhead lines which may cost from $7,000 to $20,000 per route-mile (four wires) depending on the conditions and the degree of permanence desired. The routing of trolley-coach lines warrants the most serious study. Not only is it wise to analyze shifts in population and travel habits to forecast their trends, but it is doubly desirable to lay out routes with as few crossings and turnouts as possible. Not only does complicated overhead greatly increase the first cost, but it slows down operation and increases maintenance cost.

However, first cost does not tell the whole story. It is apparent that the operating expenses of streetcars must be very much lower than those of motorbusses to offset their greater fixed charges. The fixed charges of the trolley coach obviously fall between the two, being materially less than those of the streetcar with its tracks, but still somewhat greater than those of the motorbus which uses no overhead. On the other hand, the operating expense of the trolley coach is so low that there is a very wide field where its total annual costs are lower than those of either streetcar or motorbus.

Gross Revenue

One of the features in which the trolley coach excels is its ability to attract and retain new patronage. The reasons for this passenger appeal are evident. The trolley coach is cushioned on rubber tires. It is not tied to rails like the streetcar,

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and therefore it is not subject to the annoying delays caused by broken-down or improperly parked automobiles and trucks. It loads at the curb. On the other hand it affords a higher type of transportation service than the motorbus. Its light and heat are obtained from central-station power; hence, both are ample. Adequate ventilation is provided. The trolley coach is free from the odors of hot oil and exhaust gases. Its accelerating rate is high, but there are no jerks due to interruption of torque during gear shifting as on motorbusses. Electric and air braking combine to give maximum safety and reliability The trolley coach is the quietest public transportation vehicle.

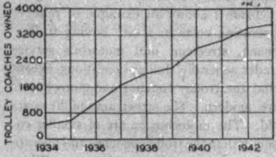

Figure 1. Ten-year record of trolley coaches owned in the United States

The result of these superior characteristics is increased patronage. In one West Coast city the number of passengers on one route increased 15 per cent and on another 20 per cent when trolley coaches were substituted for streetcars. In an industrial city in Ohio where several lines were converted from motorbus and streetcar operation to trolley coaches, the revenue per vehicle-mile increased 20 per cent, and operating costs per vehicle-mile decreased 14 per cent. Gross revenue on one line in a large eastern city increased 47 per cent, although the system earnings during the same period decreased. On another line in the same city the increase in gross revenue was 21 per cent. In one southern city a new trolley-coach installation had a gross revenue increase of 47 per cent compared to an increase of 10 per cent on representative rail lines. In one eastern city 18 rail lines were converted to trolley-coach operation over a ten-year period. In every case patronage and revenue were increased.

Operating Expenses

Not only are trolley coaches able to increase gross revenues, but they are very economical in operation. Trolley coaches have been built in sizes seating 30 to 56 passengers, but most coaches have been of 40- to 44-passenger capacity. The discussion of operating expenses will be limited to that size.

WAY AND STRUCTURES

The maintenance of way and structures is now approximately 0.8 cent per coach-mile, compared to two to four times that amount per street-car-mile and 0.1 to 0.3 cent per bus-mile. The principal expenditure is for overhead lines, and great progress has been made in line construction as well as in current collectors. In the line material all parts attached to span or guy wires such as frogs and crossings are substantial castings to which are bolted renewable runners. These wearing parts can be renewed easily and quickly without disturbing the original construction. High-speed rigid curve segments with continuous wire underrun greatly reduce the complexity of the system of span and guy wires. These devices result in lower maintenance.

Another source of maintenance expense has been the gouging of the trolley wire adjacent to "special work" by the collectors. Proper design of frogs and crossings to avoid tilting of the collector not only reduces wire maintenance but also permits operation at higher speed.

The earlier trolley coaches used wheel trolleys similar to those used on streetcars, except that swivel harps allowed the coach to operate at either side of the overhead wires within the range of the trolley poles. The wheel trolleys caused considerable arcing and resultant short life of wire. They were noisy, and even with high contact pressure dewirements were too frequent. Wheel trolleys were soon superseded by bronze or steel shoes which overcame some of the difficulties. It was necessary to lubricate the wire for metallic-shoe operation. This was an expensive procedure, and excess lubricant was deposited on the coaches and the street.

The success of carbon inserts in locomotive pantograph trolleys led to experiments in the use of carbon insert shoes. A satisfactory renewable carbon insert shoe has now been developed and is used extensively. The low contact pressures, self-lubrication, and current-collecting characteristics of the carbon are resulting in great improvement in life of overhead construction. As a result of these devices which are now available and probable future progress in design, material reductions in operating costs may be anticipated.

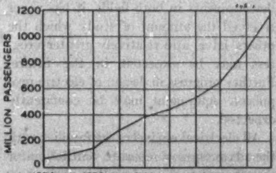

Figure 2. Ten-year record of transit passengers carried by trolley coaches in the United States

MAINTENANCE OF EQUIPMENT

Progress, which has been made in the vehicles themselves and the electric propulsion equipment, has effected economies in maintenance. The earlier 40-passenger coaches were propelled by two 65-horsepower motors. These were succeeded by a single 125-horsepower motor with a simplified control and with a double-reduction spur and bevel gear (or spur and hypoid) axle which proved to be superior in service to the worm-drive axles previously used.

Early trolley coaches had two principal items of maintenance: current collection and brakes. The former has been discussed. In the early days brake drums and linings were the same as those used on motorbusses. The trolley coach operates at higher speeds, and as a result there are more brake applications per hour, and those applications are made at higher vehicle speeds. Also the braking effect of an engine is absent. Life of brake lining and brake drums was very short. The single-motor axle provided more adequate braking equipment, but further improvement was desired.

This requirement was met by the introduction in 1938 of 140-horsepower motors with dynamic-braking control. A relatively small reduction in temperature of brake linings gives a relatively large increase in lining life. Standard practice is to provide an electric-braking rate of 2.0 miles per hour per second, whereas the total service braking rate is 3.0 to 4.0. This reduction of duty on the mechanical brakes has been sufficient to increase brake-lining life to five or six times the life obtained without electric braking. The life of the brake drums should be as long as the coach life. Reduced mechanical braking reduces tire temperature and thereby increases the life of the tires. More than 97 per cent of the trolley coaches purchased since 1938 are of the electric-braking type.

Trolley-coach maintenance in recent years has been approximately 2.70 cents per coach-mile, 0.90 cent of which is for tires and tubes. This amount should decrease as the mileage of modern vehicles expands. Modern streetcar maintenance is 1.7 to 2.0 cents per mile. The maintenance of motorbusses of comparable size averages 4.0 to 5.0 cents per mile.

POWER

The amount of power required for trolley coaches varies widely, depending on the conditions of operation. Greater weight, higher schedule speeds, hilly routes, and similar conditions increase energy consumption. The average energy consumption of 40-passenger trolley coaches according to recent American Transit Association data is 3.8 kilowatt-hours direct current per coach-mile. A modern streetcar requires approximately 5.4 kilowatt-hours per car-mile. The average cost of power is close to 1.0 cent per kilowatt-hour direct current.

The average cost of fuel and lubricants for 40-passenger motorbusses under similar conditions is approximately 4.0 cents per coach-mile, excluding gasoline tax which may be another 2.0 cents per coach-mile. Lubrication is a negligibly small item on trolley coaches but on motorbusses it is ten per cent or more of the fuel cost.

The central-station industry has a long record of decreasing the unit cost of power. Not only is the present power cost for the trolley coach favorable, but the prospects are that it actually will decrease. On the other hand, the future price of oil and gasoline is almost sure to increase. There is disagreement among the experts as to the adequacy of our supply of oil, even if we change our wasteful methods of use. One cannot expect any reduction in taxation of liquid fuels but should be prepared for increases.

CONDUCTING TRANSPORTATION

The principal item in public transportation expense is the wages of vehicle operators. At the present time wages per hour vary widely in different localities and among different types of vehicles. However, the cost of wages per vehicle-mile is inversely proportional to the schedule speed. The practically unlimited supply of power and the uninterrupted acceleration of the trolley coach give it marked advantage. It is true that some abnormally high-powered motorbusses may equal trolley-coach schedules, especially on brief test runs. However, in the same or identical service the trolley coach maintains schedule speeds 10 to 15 per cent higher than the usual motorbus. The power of trolley-coach propulsion equipment does not deteriorate with age and is practically independent of variations in air temperature and barometric pressure. Also the trolley coach is so easy to operate that driver fatigue does not reach the point where it affects schedule speeds.

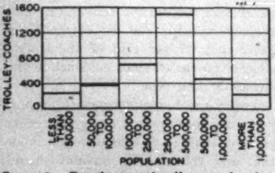

Figure 3. Distribution of trolley coaches by population groups

Table I

	Trolley Coach	Street-car	Motor-bus
Average seating capacity	44	52	29
Average passengers per one-way trip	31.8	38.4	17.9
Passengers per vehicle-hour	80	80.5	62
Operating cost per vehicle-hour less administrative and supervisory expense, per cent	100	141	107
Operating cost per passenger, per cent	100	140	138

In the long run administrative and general expense should be essentially the same for a given system regardless of the type of vehicle. At the present time different systems of accounts, different conditions with respect to insurance, and other items cause wide variations.

Future Prospects

It is believed that the future of the trolley coach is particularly bright. New materials and new methods will result in lighter, more economical, and more attractive coaches. Also the motor bus has profited from the development of millions of private automobiles and tens of thousands of motorbusses. The modern streetcar is the result of an intensive development program costing hundreds of thousands of dollars. While the trolley coach has taken advantage of developments in both fields, it has never received the amount of study which the others have, and relatively greater progress may be expected in the future. Further progress in design of electric propulsion equipment may be confidently expected.

All electrical engineers are familiar with the advantages of separate ventilation for the efficient cooling of electric apparatus. Clean dry air results in improved motor commutator and brush performance especially where the air for self-ventilation has been full of dust and dirt. In the case of the trolley coach the motor losses stabilize the temperature of the air which is later blown over the resistors for coach heating. In many installations of dynamic-braking equipment, the resistor losses during acceleration and braking are frequently adequate for coach heating with only a small amount of supplemental heat from the line. In the colder climates a large proportion of the motor losses can be used for coach heating in the winter months. The fans designed for ventilation and running at constant speed require less energy than fans for motor self-ventilation which operate over the whole range of motor speed.

Table I shows an example of relative operating costs in a city having trolley-coach, streetcar, and motorbus service under generally similar conditions, except that the trolley coaches and motorbusses are modern. The streetcars are 20 years old. The motorbusses are of smaller capacity than the trolley coaches and operate under easier traffic conditions. When conditions permit, it is expected that trolley-coach service will supersede the streetcar service on all present carlines.

Figure 1 shows the number of trolley coaches owned in the United States by years from 1934 to 1943. As of December 1, 1943, there were 3,502 trolley coaches.

Figure 2 shows the number of passengers carried by trolley coaches each year from 1934 to 1943. Trolley-coach passengers were 0.6 per cent of total transit passengers in 1934 and 5.3 per cent in 1943. In cities between 50,000 and 1,000,000 population the trolley-coach passengers were 8.7 per cent of the total in 1943.

The number of trolley coaches owned in cities of various population classifications is shown in Figure 3. Cities between 100,000 and 1,000,000 have 76 per cent of the total; those between 250,000 and 500,000, 43 per cent.

Conclusion

Generalizations are frequently misleading, because there is no substitute for detailed analytical study of any problem. However, a number of studies indicate that the proper economic field for the trolley coach is that where the interval between vehicles during base hours is not less than four or five minutes and not longer than 15 to 20 minutes. It is realized that both of these limits vary widely under different conditions and that other considerations such as political

Aircraft Fuses Must Protect

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Synopsis: The ever-increasing list of electric equipment needed for the satisfactory performance of the modern airplane has brought the electric system from a place of relative unimportance to one which cannot be ignored. The electric equipment must not fail, particularly on combat planes. Therefore, the protective devices must be designed and applied to permit the equipment to function as long as it can supply any useful work but to remove it from the circuit when it has ceased to function or has become a hazard. This type of electrical protection is obtainable with fuses of special design when applied properly.

THE evolution of the electric system in aircraft has paralleled the development of the airplane itself. The original six-volt d-c electric system was little more complicated than that employed in the automobile. As power requirements increased, the voltage was increased to 12, then to 27 volts, which was considered adequate for several years. Increased electrical demands have forced the voltage still higher to 120 volts direct current in some installations, and now a 208-volt three-phase 400-cycle system has ceased to be merely a dream but will become a reality in the not too distant future.

Unfortunately, the transition from one system to the next has been so gradual that the whole problem of circuit and equipment protection has not been reviewed to see if a completely new or unorthodox approach could effect better results. The techniques of the six-volt system more or less have been transplanted to the higher-voltage systems without re-evaluating them in the light of the requirements of the new system. Fortunately the 208-volt three-phase 400-cycle system will not permit such a transition, so that now is an ideal time to analyze what is available and what has been used to obtain more complete electrical protection in aircraft.

Fusing Requirements

The fusing requirements in aircraft differ little from the results desired in industry except that the requirements and demands of the former are much more stringent. To save weight the overload capacity of aircraft electric equipment is reduced to an absolute minimum, whereas in normal power applications extra weight is not so objectionable.

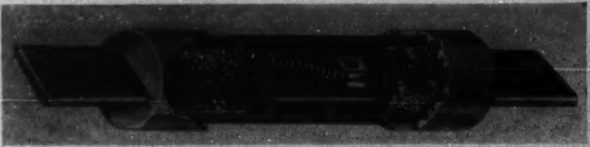

Figure 1. Time-delay fuse consisting of a heater, a thermally operable current interrupter, and a fast-acting short-circuiting member

Also, in aircraft the range of ambient temperatures to which a given installation is subjected is much greater than that found in normal power work. It is true that a power installation can be found where extremes in ambient temperatures may be experienced, but seldom, if ever, is one installation ever subjected to the range of -60 to $+71$ degrees centigrade as found in aircraft. Useless or unnecessary operation of protective devices in airplanes cannot be countenanced, particularly in military applications, whereas in most power applications objectionable operation of the protective device is seldom little more than a nuisance, hardly ever a catastrophe. Then the weight of the protection cannot be ignored in aircraft, thereby eliminating the use of many devices which have been proved satisfactory in industry. Hence, aircraft fusing can learn much from the techniques already employed in power work, but it must go further in accuracy to give the trouble free but complete protection required.

Electric fusing, whether it is applied to power work or aircraft, may be divided into two general categories:

1. Fuses to protect electric equipment.
2. Fuses to protect the circuit itself.

Consider first the practice now employed in power work. Since aircraft electric system voltages will not exceed 208 volts, the discussion can be limited to secondary power fusing.

Protection of Equipment

Electric equipment, with the exclusion of instruments and similar devices having practically no overload capacity, consists of a current-conducting path, usually metallic; insulation, either organic or inorganic; and a heat-absorbing mass, usually iron. The current-conducting path, insulation, and heat-absorbing mass are usually in good thermal relation, so that, under steady-state conditions, the heat generated in one member will elevate the temperature of all three components. Since the purpose of the electric equipment is to convert electric energy to mechanical, thermal, or chemical energy, and its efficiency is never 100 per cent, there always will be some electric energy converted to heat in the device so increasing its temperature.

When the equipment is subjected to slight overloads which are continued for appreciable times, failure will occur in the material which is damaged at the lowest temperature. Because there is time for heat conduction, the temperatures of the component parts of the equipment are elevated by the heat generated in the current-conducting path. This does not mean that there is not a temperature

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or financial affiliations sometimes dictate the course to be followed.

In the great majority of cities where trolley coaches have been tried on a reasonably large scale, their numbers have increased rapidly. Trolley coaches are the backbone of Seattle's transportation system, 292 being in use. Milwaukee has 274 coaches, Providence 204, Boston 162, Indianapolis 162, and Chicago 152. All of these latter cities started with relatively small initial installations and expanded them as a result of the favorable economic results. All of them contemplate further installations.

gradient in the device, but it does mean that this gradient remains unchanged if the load is maintained long enough to approach a stable condition. The point or temperature of failure, of course, is dependent upon the individual characteristics of the component parts of the device, but, once the various materials have been chosen, this point is established irrevocably. The overload may be the results of the malfunctioning of the electric equipment itself or may be due to a greater connected load than was anticipated in the design, but, in either case, the result is the same. In the final analysis this type failure may be regarded as a temperature failure even though it is caused by heat produced by electric energy. Hence, the condition is aggravated when operating in high ambient temperatures and alleviated in the lower ambient temperatures.

If the overload is increased, the length of time the equipment can withstand it is shortened, but, again, the mechanics of the failure is the elevation of the temperatures of the various component parts in their relative proportions until one fails. This process continues as the overloads are increased, the safe time steadily decreasing, but always dependent upon the thermal capacity of the whole device and influenced by the ambient conditions to which it is subjected. However, at some overload the heat generated in the current-conducting path will be so great that it will fuse before there is time to conduct some of the heat away from it to those components injured at a lower temperature. This type failure may be considered to be free from ambient effects, because the variation in ambient temperature, though theoretically affecting the failure in that less heat would be needed to cause failure at the higher ambient temperature, is so small a percentage that, for all practical purposes, its effect may be regarded as negligible. To illustrate the general theory, consider an electric motor. The current-conducting path is the copper wire; the insulation is enamel, rubber, glass, silk, or cotton used on the field windings; the heat-absorbing mass is the frame and rotor. On the lower overloads failure results from the insulation on the windings being destroyed, whereas on the higher overloads the copper in the winding itself actually fuses.

From this it is seen that the curve of the overload capacity or safe-operating characteristic of an electric device contains a point of inflection, because it is the combination of two curves having different slopes. The time–current curve of the copper is steep, whereas the curve in that region in which there is time for thermal conduction has a more gradual slope and is affected by ambient temperatures. A protective device, to give complete protection over the entire range without useless blows, must have the same characteristic. This characteristic cannot be produced by a single-element device, whether it be a fuse, circuit breaker, or thermal cutout. One answer to the problem is found in the time-delay fuse shown in Figure 1 time–current curve of which is shown in Figure 2. This device consists of a thermally operable current-interrupting device in series with a fast-operating short-circuiting member. Current flowing through the fuse generates heat in the heater as well as in the short-circuit strip. At the lower overloads most of the heat is supplied by the heater, but, as the overload is increased, the effect of the short-circuiting strip becomes more pronounced until finally, on the very high overloads where there is no time for thermal conduction, the fast-acting short-circuiting strip fuses before the thermally operable current interrupter can function. The time delay or time lag is obtained on the low overloads, because the heat generated in the heater must be conducted into the center member, called the absorber, and must elevate its temperature to the melting point of the fusible alloy holding the trigger before it can operate opening the circuit. The characteristics of this device are adjusted to approach the safe-operating characteristics of power electric devices, so that, if its rating is chosen correctly, complete electrical protection without useless blows is obtained. The melting point of the fusible alloy is chosen, so that the effect of ambient temperatures on the fuse is the same as on the electric device it is protecting. The time-delay fuse gives wonderful flexibility in design, because the heat-generating member, the fusible alloy, the mass of the absorber, and the characteristics of the short-circuiting strip can be adjusted independently giving any characteristic desired.

The United States Army Air Corps appreciated the potentialities of this type fuse and built their specification 94-32084 around it. Since, at the time the specification was written the highest system voltage in use was 27 volts, the short-circuiting characteristics were not specified. The specification covers the lower end of the curve by requiring the fuse to carry 110 per cent load indefinitely and to open at 125 per cent load within one hour and at 200 per cent load between 90 seconds and eight minutes. The omission of the unnecessary short-circuit requirements in the 94-32084 specification permitted a desirable saving in size and weight.

The fuse combining a fusible element with a thermally operable current interrupter is often condemned and precluded from aircraft applications, because it is felt that the increased protection is obtained only by increasing the weight which cannot be justified. Actually the use of such a fuse decreases instead of increases the weight of the protective device. This seeming anomaly is true, because the rating of the protective device can be decreased when time lag is built into it. To illustrate, consider a three-phase ten-horsepower 220-volt motor connected in series with ordinary fuses. Even though the full-load current of the motor is 27 amperes, 90-ampere fuses are used in the circuit to assure that the fuses do not blow under starting or transient conditions. Such a device only can furnish protection to the motor above 90 amperes. The fuse itself, neglecting the fuse block in which it is mounted, weighs 121 grams. If, instead of the conventional fuse, a fuse having a thermally operable circuit interrupter as well as a short-circuiting strip is used, the rating of this device can be reduced to 30 amperes, and still it has sufficient time lag to withstand starting and transient currents. With such a device the motor is protected over its entire range of overloads and short circuits by a device weighing only 20 grams. Therefore, the change from a conventional fuse to a time-delay fuse not only improves the protection obtained but, at the same time, effects a very desirable saving in weight, the time-delay device weighing only one-sixth that of the conventional fuse. If the blocks in which the fuses are mounted also are considered, the saving in weight is even more pronounced.

The counterparts of the commercial power fuses which have time lag and are of conventional design are found in the Air Corps specification 94-32084 and 32084-*B*. Specification 32084-*B* did not require the fuse to have any inherent time lag, its characteristic being comparable to a power fuse of conventional design. As already mentioned, specification 94-32084 required the time lag found in fuses combining a thermally operable current interrupter with a short-circuiting member. A 100-ampere 32084-*B* fuse needed for a 27-ampere motor weighs 71 grams, whereas a 30-ampere fuse made in accordance with specification 94-32084, giving complete protection to a 27-ampere motor, weighs only 22 grams. Therefore, contrary to the general conception, complete protection is effected with a saving in weight instead of increasing the weight of the fuse, even though ampere for ampere the time-delay fuse weighs more.

As the ceiling of aircraft has been pushed steadily upward, the ambient-temperature range to which it is exposed has increased until now −60 to +71 degrees centigrade is not unreasonable to expect. Since the usual procedure is to mount the electrical protective devices in a central location, the equipment seldom would be exposed to the same ambient temperature as its protective device. Such a central or grouped mounting is necessary when circuit breakers combining a switch with electrical protection are used but is unnecessary when the switching operation is divorced from the protector as is possible with fuses. Then the fuse can be mounted wherever convenient, and, if the type and size fuse is chosen intelligently, the ease of replacement need not be a factor. The fuse only will operate when there is a case of trouble and the electric equipment it is protecting already has been operated to its thermal or electrical breaking point, so that replacement before clearing the trouble accomplishes nothing.

The extreme ambient requirements imposed upon all the airplane electric equipment have been appreciated, but no reasonable solution is possible so long as the electrical protective devices are subjected to ambient temperatures different from those to which the electric equipment itself is exposed. Since the normal grouping now employed in aircraft precludes the possibility of the equipment and protective-device ambient temperatures being the same, the effect is alleviated partially by making the electrical protective device irresponsive to ambient temperatures and then building enough overload capacity in the electric equipment so that it will function satisfactorily at the higher ambient temperature. Since the capacity of most electric equipment decreases with increasing temperature, it is overfused at the lower ambient temperatures and underfused at the higher ones, because the protective device is designed to operate purely on current, not ambient temperature. The net result is that accurate fusing is impossible as long as the protective device is not responsive to temperature in the same manner as the equipment it is protecting and as long as the time–current curve is not identical over the entire range of currents which flow through the equipment.

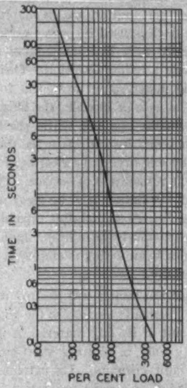

Figure 2. Time–current characteristic of the time-delay fuse shown in Figure 1

The time-delay fuse already described can be biased to give the same response to ambient conditions as the electric equipment it is protecting by choosing the operating point of its thermal interrupter properly. Since the operation of the thermal interrupter in the fuse is dependent on temperature resulting from the heat generated in the heater of the fuse as well as on the ambient temperature, the bias needed to compensate for any given range of ambient temperatures can be obtained by the choice of the operating point of the thermal interrupter. An even closer thermal relationship between the electric equipment and its protector is accomplished in power applications by mounting the time-delay fuse on the equipment itself in good thermal relation to the hot spot. This thermal relation is obtained by a metal pin which extends into the equipment from one terminal of the protective device. To improve the flexibility of the design, two points are provided which are responsive to thermal energy. The first, located between one terminal and the heat coil (see Figure 3), operates almost entirely from the heat supplied it by thermal conduction from the hot spot of the device being protected. The second thermal interrupter, located between the heat coil and the short-circuiting strip, is biased very slightly by the heat conducted to it from the hot spot, through the heat coil. In most cases the temperature of the heat coil is above that of the terminal, so that heat conduction from the terminal to the second interrupter through the coil is impossible, but the biasing effect of the thermal conduction from the hot spot is realized in that the terminal temperature is elevated by it, so reducing the temperature gradient from the thermal center of the coil to the terminal and therefore reducing the heat conduction, thereby forcing more of the heat generated in the coil to flow into the second thermal interrupter. The operation of the thermal interrupter between the heat coil and the short-circuiting strip is due primarily to the heat generated in the heat coil and is a function of the current flowing through it. Since the heat must be conducted from the coil to the interrupter, a time lag is obtained which can be varied by varying the mass of the thermal interrupter or by varying the length of the thermal path through the heat coil. The short-circuiting strip, of course, is designed to be fast acting, but its rating is so chosen that it does not function until the higher overloads, approaching short-circuit conditions, are realized.

Such a protective device has proved its worth in industry. The flexibility of its design permits the time–current curve to be made the same shape as that of the equipment it is protecting, so that complete protection is furnished over the entire operating range of the equipment under any ambient conditions. It will operate, protecting the equipment, whenever the temperature of the hot spot exceeds a safe predetermined temperature. Again the only answer to the objection that the protective device is unsuited for aircraft applications, because it often must be mounted in an inaccessible place which cannot be reached in flight, is that such a protective device eliminates the guesswork in fusing so that the protective device only will function when the equipment is worthless. The continued use of the equipment under such conditions only will disturb the electric system if not injure it without producing any useful work. If the equipment itself is accessible enough to be repaired in flight, then, obviously, the protective device also is replaceable, so that, in either case, the positive reliable performance of a properly

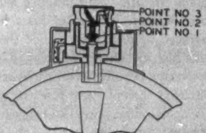

Figure 3. Time-delay fuse consisting of three interrupters in series

Point 1 is responsive to the heat conducted to it from the device being protected; point 2 is responsive to the heat conducted to it from the heat coil; and point 3 is a fast-acting short-circuiting member functioning at high overloads designed and properly selected protective device cannot be criticized.

Such a time-delay fuse never has been adapted to aircraft applications, but it could be made quite attractive from a weight angle, because the present commercial model, in which weight has not been given any special consideration, weighs only 23 grams for a ten-ampere rating, whereas a 94-32084 fuse of the same rating weighs 18 grams. Hence, a few minor design changes probably could reduce the weight to a figure quite comparable with that of the 94-32084 fuses which have proved quite satisfactory.

Circuit Protection

The problem of circuit protection as distinct from the protection of electric equipment is being attacked from two angles, both of which have their advantages and limitations. The first may be termed circuit isolation, its principal function being to isolate heavy faults and remove the load from the system to prevent the overloading of the generators supplying it. The second may be termed circuit protection in that the protective device is chosen so that it will function as a result of damaging overloads as well as faults, to remove the excessive load from the system. In either type of circuit protection, fuse co ordination, both with the branch circuit fuses and the equipment fuses, must be considered to prevent cascading and the blowing of fuses which will de-energize more than the absolute minimum of equipment under fault conditions. Fortunately, on the 27-volt systems the co-ordination problem was quite simple, because a transmission system of the simplest sort was employed, and the number of points at which cascading could occur was few if any. Because of this, fuse co-ordination was accomplished by increasing the rating of the feeder fuses inordinately to prevent them blowing under fault conditions in any of the branch circuits. This practice led to the use of limiters instead of fuses in these overfused applications. Since the rating of the fuse was chosen so great that under low overload conditions the wire would be destroyed before the fuse functioned, nothing could be gained by using a fuse.

Limiters differ from fuses in that the fusing current of a limiter is great compared to the safe carrying current, whereas in a fuse the difference between the carrying current and the opening current is small. Since the limiter link usually is made of copper, silver, or other high-melting materials, there is a range of currents between the safe carrying current and the fusing current which, if continued long enough, will destroy the device itself as well as equipment in good thermal relation to it. The fuse, on the other hand, will carry or blow, depending upon the current, without ever reaching a damaging temperature. Hence, the current limiter can be compared to the fast-acting short-circuiting strip which is combined with the thermal interrupter in the time-lag fuses already described. The aircraft limiters are an adaptation of those devices used in power work which originally were little more than a cable lug with a reduced section. Their function in underground work was to assure that, in case of trouble, the blow would occur in the manhole where the limiters are mounted instead of in the duct run, thereby simplifying the clearing of trouble and the restoration of service. This feature of the limiter is of no great value in aircraft where the conduit runs are short and particularly in military applications in which open wiring is employed almost universally.

Since the limiter will destroy itself on slight overloads because of the high melting point of its link material, it is satisfactory only in clearing short-circuit or high fault currents. Its extremely low time lag gives a wider separation of blowing times between limiters of different ratings at the same current, so that coordination is relatively simple. Therefore, limiters permit the isolation of faults producing high fault currents, but on low currents they are worse than no protection at all, because they in themselves produce a fire hazard and destroy equipment associated with them.

The fault currents resulting in an electric system are dependent not only on the impedance of the fault but also on the capacity of the generating equipment. Of course, the higher the voltage, all other conditions being equal, the greater will be these currents. Hence, on the 27-volt systems the fault currents may be expected to be relatively small, whereas those encountered in the 208-volt systems will assume reasonable proportions. However, the impedance at 400 cycles will reduce these currents, so that it is not at all unreasonable to assume that low fault currents can be obtained in aircraft. As long as these currents are not great enough to injure the wire insulation, there is no reason for them to be cleared instantly, particularly if the isolation of the fault will, at the same time, isolate equipment which is functioning properly. Instead, it would be better to permit the fault to persist until the completion of the mission and then be cleared when the airplane had returned to its base. Many of the wire sizes in aircraft are chosen from a voltage-drop standpoint instead of a thermal one, so that low fault currents will produce no damage to the distribution system and will affect the equipment only in that the voltage at its terminals will be reduced because of the increased voltage drop in the line. If this drop is sufficient to cause the equipment to overheat, it will be protected by its own protective device, so that the circuit protective devices need not function in this eventuality. If the fault current is so great that the thermal capacity of the wire itself is insufficient to withstand the heat generated in it, the fault must be isolated if complete protection is to be realized.

To obtain complete protection of the electric distribution system, the problem again reverts to a consideration of the combined thermal capacity of the insulation and copper of the conductors. Again the protective device's time–current characteristic must be identical with that of the conductor to give complete protection under all conditions of load. Since the thermal capacity of the conductor is less than that of most electric equipment, the time lag of the circuit protective device need not be so great as that of the equipment protection. This problem was appreciated by the United States Army Air Corps and was covered by specification 94-32272. The time lag required in the most recent issue of this specification is a minimum of 40 seconds as compared to 90 seconds in the 94-32084 specification covering protective devices for electric equipment.

The effect of ambient temperature on the carrying capacity of the wires is as important as its effect on the capacity of electric equipment, so that again a protective device responsive to ambient temperatures in the same proportion as the wires and mounted in the same ambient temperature is needed. This is not so unreasonable as it appears on the surface, because the use of glass insulation on the wires permits a relatively high safe-operating temperature, so that the effect of the ambient temperature on the protective device need not be too pronounced. Also, the grouping of sectionalizing fuses in a common central cabinet which may be subjected to ambient temperatures very different from those of the wire is unreasonable, both from a safety angle in military aircraft and from a functional angle. Grouping the devices together necessitates the use of additional wire which, in turn, should require additional sectionalizing fuses for complete fault isolation.

Therefore, the circuit fuses can be mounted in approximately the same ambient temperature as the circuit wires and, though not so responsive to ambient temperature as the equipment protection, still should respond in the same manner as the circuit conductor.

By using a time-lag fuse of the design shown in Figure 1 but with the absorber mass decreased and the thermally operable current interrupter biased to be less responsive to ambient conditions than the equipment protective device, a saving in weight is accomplished, and a circuit protective device is obtained which will co-ordinate beautifully with the equipment protector. Since both the equipment and the circuit protectors have fast-acting short-circuiting members, excellent co-ordination is obtained at the higher overloads with complete protection to equipment and circuit under every conceivable condition of overload and ambient temperature. Thus, by adapting an arrangement already proved in power applications, aircraft can avoid many headaches, if not catastrophes, as its power system grows from that of an automobile to that of a power network.

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Switchgear and Control for an Electronic Power Converter

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Synopsis: The electronic power converter, controlled by the equipment described in this paper, consists of two units rated 10,000 kw installed and operating at the Edgar Thomson Works of the Carnegie-Illinois Steel Corporation. The power converters connect a 60-cycle 69-kv system and a 25-cycle 44-kv system with a permissible power transfer of 20,000 kw in either direction between the systems. Transfer of power by rectification and inversion and the design of the electronic power converter are discussed in companion papers.[1,2] The switchgear and control devices are discussed in this paper and for convenience and clarity, the discussion is divided into three main headings: "Switching," "Control," and "Protection."

To the switchgear designer whose point of view is influenced so largely by the needs of the "man who operates and maintains," the electronic power converter presents some new and interesting opportunities. The ease with which the converter is switched into and out of service and its output is controlled presents the opportunity of providing simple and at the same time versatile controls by means of which the operator can make the fullest use of the converter, in meeting the varying demands of his system. Protective relaying of the electronic converter against system disturbances is simple since the rectifier furnishes no power to its system and the output of the inverter is limited.

Switching

An examination of Figure 1 will reveal that the switching of the main power circuits of the electronic power converter is of the simplest possible nature. The unit is switched into service merely by closing power circuit breakers connecting the transformers to the two systems. Aside from having the auxiliary and ignition bus energized by closing the auxiliary and ignition transformer switch and circuit breaker, the only prerequisite to putting the unit into service is that the ignition-control-system networks for both frequencies must be in the inverter position. Auxiliary switches on the circuit breakers and position switches on the power-control device provide circuits for establishing this condition whenever the circuit breakers are tripped, and simple interlocking in the circuit-breaker control circuits insures it. Therefore, the sequence of closing the breakers is of no consequence, and either breaker may be closed first, regardless of the intended direction of power flow. For convenience and simplicity of protective-relay circuits, both breakers are tripped simul-

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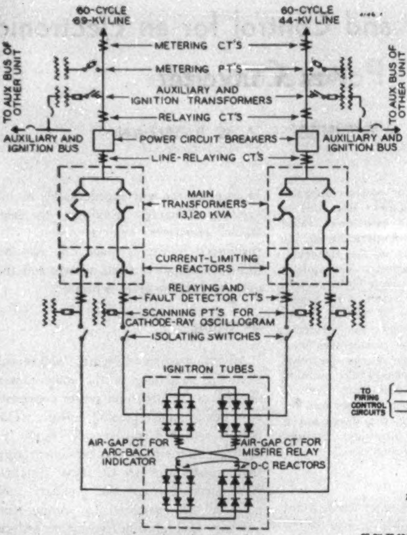
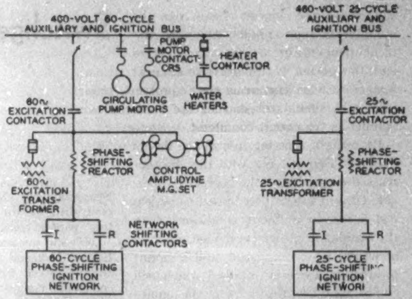

60-CYCLE
69-KV LINE
60-CYCLE
44-KV LINE
METERING CT'S
METERING PT'S
AUXILIARY AND
IGNITION TRANSFORMERS
RELAYING CT'S
TO AUX BUS OF
OTHER UNIT
TO AUX BUS OF
OTHER UNIT
AUXILIARY AND
IGNITION BUS
POWER CIRCUIT BREAKERS
AUXILIARY AND
IGNITION BUS
LINE-RELAYING CT'S
MAIN
TRANSFORMERS
13,120 KVA
CURRENT-LIMITING
REACTORS
RELAYING AND
FAULT DETECTOR CT'S
SCANNING PT'S FOR
CATHODE-RAY OSCILLOGRAM
ISOLATING SWITCHES
IGNITRON TUBES
AIR-GAP CT FOR
ARC-BACK
INDICATOR
AIR-GAP CT FOR
MISFIRE RELAY
D-C REACTORS

Figure 1. Simplified one-line diagram showing switching equipment


460-VOLT 60-CYCLE
AUXILIARY AND IGNITION BUS
460-VOLT 25-CYCLE
AUXILIARY AND
IGNITION BUS
PUMP
MOTOR
CONTACT-
ORS
HEATER
CONTACTOR
CIRCULATING
PUMP MOTORS
WATER
HEATERS
60~
EXCITATION
CONTACTOR
25~
EXCITATION
CONTACTOR
PHASE-
SHIFTING
REACTOR
CONTROL
AMPLIDYNE
M.G. SET
60~
EXCITATION
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PHASE-SHIFTING
IGNITION
NETWORK
25-CYCLE
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IGNITION
NETWORK

Figure 2. Switching equipment for auxiliary apparatus

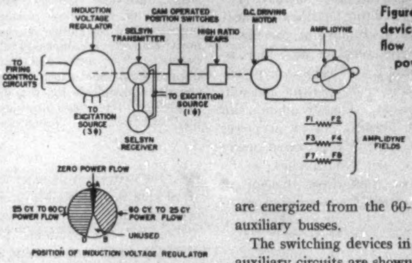
INDUCTION
VOLTAGE
REGULATOR
SELSYN
TRANSMITTER
CAM OPERATED
POSITION SWITCHES
HIGH RATIO
GEARS
D.C. DRIVING
MOTOR
AMPLIDYNE
TO
FIRING
CONTROL
CIRCUITS
TO EXCITATION
SOURCE
(1 ϕ)
TO
EXCITATION
SOURCE
(3 ϕ)
SELSYN
RECEIVER
F1
F2
F3
F4
F7
F8
AMPLIDYNE
FIELDS
ZERO POWER FLOW
C
A
25 CY TO 60 CY
POWER FLOW
60 CY TO 25 CY
POWER FLOW
UNUSED
D
B
POSITION OF INDUCTION VOLTAGE REGULATOR

Figure 3. Control device for power flow through the power converter

taneously at all times. Circuit-breaker operation is the only switching operation required under normal operating conditions.

The only switching required in the circuits between the ignitron tubes and the transformers is that required for safety to personnel who may have occasion to work on equipment in the tube assembly enclosure. For this purpose, enclosed group-operated disconnecting switches are provided, suitably key-interlocked with tube-enclosure doors and high-voltage power circuit breakers to insure safety and to prevent operation of the switches under load.

Since the flow of power in the d-c portion of the circuit is under complete control of the grids of the rectifier and inverter tubes, there is no necessity for any switching apparatus in the d-c circuit.

Switching of the auxiliary and ignition transformers to the high-voltage lines is accomplished by horn-gap disconnecting switches suitable for interrupting magnetizing current of the transformers. These switches are suitably key-interlocked with the transformer secondary air circuit breakers to prevent their operation under load. The transformers are protected by suitable current-limiting fuses.

The 460-volt secondary windings of the auxiliary and ignition transformers are connected to their respective loads by means of manually operated air circuit breakers. The transformers have sufficient capacity to carry the auxiliary and ignition load of both units. Provision is made therefore by means of a tie air circuit breaker for energizing both unit auxiliary busses from one transformer in case the other is out of service for any reason.

Each power converter requires the following general auxiliary devices, which for the sake of utilizing standard apparatus are energized from the 60-cycle auxiliary bus:

Driving motor for main control Amplidyne generators.

Cooling-water circulating-pump motors.

Water heaters.

The phase-shifting networks which supply the ignition equipment for the 60- and 25-cycle tube assemblies are energized from the 60- and 25-cycle auxiliary busses, respectively, through selector contactors which determine whether the tube assembly is to operate as a rectifier or inverter.

Similarly, excitation transformers, supplying dry-plate rectifiers for the excitation of the main control Amplidynes, are energized from the 60- and 25-cycle auxiliary busses.

The switching devices in these various auxiliary circuits are shown in Figure 2.

Control

When switching of the power circuits has been completed as previously described, the power converter is ready for control of direction and magnitude of power flow. The explanation under this heading will cover the means by which the power flow through the power converter is controlled. The operator has a choice of three types of control: manual control by control switch, or either of two types of regulator control—watt regulator or load regulator. The paragraphs which follow first deal with the devices common to all three types of control, and then the three types of control are individually briefly explained.

The direction of power flow through a power converter is determined by the connections of the phase-shifting ignition networks to the auxiliary and ignition bus as discussed in a companion paper.[2] These connections are made by contactors *I* or *R* of Figure 2; when the *I* contactor is closed, the phase-shifting ignition network causes the tubes connected to that network to operate as inverters; when the *R* contactor is closed, the phase-shifting ignition network causes the tubes connected to that network to operate as rectifiers. Therefore, for example, to have power flow through the converter from the 60-cycle system to the 25-cycle system, the *R* contactor of the 60-cycle network is closed and the *I* contactor of the 25-cycle network is closed. The *I* and *R* contactors therefore are the direct means by which direction of power flow is established. The control of those contactors is described in the following paragraphs.

The magnitude of power flow through a power converter is determined by the magnitude of the reference voltage used in the field circuits of the control Amplidyne associated with the firing circuits, as discussed in a companion paper.[2] The reference voltage is obtained from an induction voltage regulator, and the magnitude of that voltage is determined by the induction regulator's rotor position. Control of the rotation of the induction-regulator rotor therefore is the direct means by which the power-converter load is controlled.

The power-control device is shown in Figure 3. The rotor of the induction regulator, the Selsyn transmitter, and the cam-operated position switches are on a common shaft; the shaft is driven through a high-ratio gear box to give smooth vernier control of shaft position. Contacts of the cam-operated position switch control the *I* and *R* contactors (of Figure 2). For the position of the induction regulator corresponding to 90 degrees retard excitation and (zero power flow) for rectifiers, as discussed in a companion paper,[2] the contacts of the cam-operated position switch cause the *I* contactors of both the 25-cycle and 60-cycle networks to be closed, and all tubes of the power converter operate as inverters. As the induction-regulator rotor is moved away from this position of zero power flow, by means of contacts of the cam-operated position switches, the *I* contactor of one of the networks is opened and the *R* contactor is closed, making the power converter capable of power transfer; for one direction of rotation of the induction-regulator rotor from its zero power flow position the *R* contactor of the 25-cycle network is closed, and for the other direction of rotation the *R* contactor of the 60-cycle network is closed. Also, as the induction-regulator rotor is rotated in either direction from the zero-power-flow position, its output voltage magnitude is varied to give smooth increase from zero to maximum power flow as discussed in a companion paper.[2] The power flow through the power converter for different positions of the induction-regulator rotor is illustrated in Figure 3 (below the induction voltage regulator); this illustration represents 360 mechanical degrees of rotation of the rotor. It is at points *A* and *C* that the *R* contactor of the 60-cycle or 25-cycle network, respectively, closes and remains closed over the travel represented by the cross-hatched areas in the illustration. Rotor rotation from *A* to *B* or from *C* to *D* represents gradual smooth increase in power flow from zero to maximum for either direction of power flow as marked. Two contacts of the cam-operated position switch act as limit switches and prevent travel of the induction-regulator rotor beyond points *B* and *D*. Indication of the position of the induction regulator is obtained on the control board from the Selsyn receiver shown in Figure 3.

Driving power for the shaft driving the induction regulator, Selsyn, and cam-operated position switches is obtained from a d-c motor whose armature is energized by an Amplidyne generator. The use of a generator rather than a constant potential d-c source to energize the d-c motor has the advantages of:

1. Reversible rotation of the d-c motor without the use of reversing contactors.
2. Easy adjustment of d-c-motor armature voltage magnitudes.
3. Easy adoption of control circuits.
4. Inherent dynamic braking.

The Amplidyne is used instead of a conventional d-c generator, because its low excitation requirements permit the use of relay and control-switch contacts directly in its field circuit; the control fields of the Amplidynes used require only 0.1 ampere for 300-volt output; also these control fields have a resistance of only 218 ohms which permits the use of low control voltages.

The Amplidyne has three fields, two control fields designated as *F*1-*F*2 and *F*3-*F*4 and one field designated as *F*7-*F*8 used for antihunt and residual voltage killing. To produce rotation of the d-c motor, voltage is applied to one of the Amplidyne control fields. Either direction of rotation is obtained as desired, since the control circuits apply a voltage of either polarity to a given field terminal. Separate "raise" and "lower" speed adjustments are provided, since operating engineers may find it desirable to decrease the load setting at a faster rate than the load setting is increased, or vice versa. A fault-blocking contact in the control-field circuits opens during arc-backs or other faults within the power converter; this prevents change in the setting of the induction voltage regulator during such faults while the fault-recovery equipment is functioning.

As stated in the section on switching, the main circuit breakers can be closed only if excitation power is available and if both the 25-cycle and 60-cycle portions of the converter are operating as inverters. To insure that excitation is present, an undervoltage relay is connected to the auxiliary and ignition bus (Figure 1), and its contact permits the breakers to close only when proper voltage is present. Contacts of the cam-operated position switch (Figure 3) prevent closing of the breakers except when the induction voltage regulator is in its zero-power-flow position where all tubes operate as inverters. If the circuit breakers are tripped while the converter is carrying load, the induction regulator automatically is returned to its zero-power-flow position.

Manual control of power flow is by means of a simple double-throw momentary-contact control switch. Amplidyne field *F*3-*F*4 is used for manual control, one position of the control switch connecting terminal *F*3 to positive potential, and the other position of the switch connecting terminal *F*3 to negative potential (control potential is 24 volts direct current). An operator, therefore, merely turns this control switch in the position to give the desired direction of power flow, and the switch is held in that position until power flow has increased to the desired magnitude as indicated by a wattmeter on the control board.

The watt regulator is a device for regulating automatically the power flow through the power converter. The power elements of the device are connected to the current transformers and potential transformers in the power circuit of the power converter (Figure 1); the potential elements are correctly connected to the potential transformers for either direction of power flow by means of contacts of the position switches. The watt regulator has two sets of contacts; one set of contacts is closed when the power-converter power flow is greater than the regulator calibration, and the other set of contacts is closed when the converter power flow is less than the regulator calibration; both sets of contacts are open when the converter power flow is equal to or within approximately ±2 per cent of the regulator calibration. Calibration of the device is changed by means of a control switch on the control board; an instrument is provided on the control board which indicates the calibration setting directly in kilowatts.

Figure 4. Control-room panels and excitation cubicles for 10,000-kw electronic power converters

Figure 5. Load-control panels for each converter unit and test panel at right

The two sets of contacts connect the Amplidyne control field to control voltage of correct polarity to cause the induction voltage regulator rotor to be rotated in the direction to correct the power flow; for example, if the power flowing through the converter is less than the setting of the watt regulator, its contacts connect the Amplidyne control field to the potential which will cause the d-c motor to rotate the induction regulator rotor in the direction to increase power flow through the power converter. Amplidyne control field *F*3-*F*4 is used for watt regulator control as well as for manual control. With the watt-regulator control switch in the "on" position, the regulator contacts are connected to field *F*3-*F*4 and the manual-control-switch contact circuits are disconnected. Contacts of the cam-operated position switch connect the regulator contacts to the control voltage polarity which gives correct direction of rotation of the induction voltage regulator for either direction of power flow in the frequency changer. In putting a power converter on watt regulator control, it is initially necessary for the operator to select direction of power flow by means of the manual-control switch; once the induction-voltage regulator has arrived at either position *A* or position *C* (Figure 3), the regulator control switch can be turned to the "on" position, and the regulator functions to pick up the load for which the regulator is set.

The load regulator is an indicating integrating device energized from current and potential transformers which measure the interchange of power between the power company's system and the industrial company's 60-cycle system. The load regulator is designed to hold a preselected value of instantaneous exchange kilowatts. This value, however, is corrected to compensate for any differential between the ideal preselected kilowatt-hour exchange and the actual kilowatt-hour exchange as time progresses.

The load regulator permits the industrial user to make the best possible exchange of kilowatt-hours with the power company as determined by his contract with the power company. When the 60-cycle industrial load is heavier than the setting of the load regulator, the power converter supplies kilowatt-hours from the 25-cycle system to the 60-cycle system; when the 60-cycle industrial load is smaller than the setting of the load regulator, the power converter supplies kilowatt-hours from the 60-cycle system to the 25-cycle system. The contacts of the load regulator connect Amplidyne field *F*1-*F*2 to the control voltage polarity which correctly adjusts the power flow through the power converter. The load regulator is made operative or is removed from operation by a two-position control switch.

For the station described, which has two converters, the load regulator can be used to control either or both converters by selecting such operation by means of the control switches. When both power converters are operated on load-regulator control, load-balance relays are made operative. The load-balance relays are two-element induction-disk devices whose current circuits are energized from differentially connected current-transformer circuits of the two power converters. These relays keep the loads on the two power converters approximately equal when both are controlled by the load regulator.

Any of the types of control described, manual, watt-regulator or load-regulator, can be used on one converter without regard for the type of control being used on the other. While the operator has a versatile choice of control combinations at his disposal, there are no complicated operating sequences which must be followed.

Protection

For the purpose of discussion of protective relaying, faults which must be considered may be classified and subclassified as follows:

1. Faults associated with the converter its transformers, and auxiliary apparatus
   (*a*). Insulation failures in the transformer and a-c conductor system.
   (*b*). Malfunction of the ignitron tubes or insulation failure in the d-c conductor system accompanied by failure or too frequent operation of the automatic fault-suppression features of the converter grid and ignition control.
2. Faults associated with the power systems
   (*a*). Short circuits.
   (*b*). Loss of voltage.

Insulation failures in the transformers and a-c conductor system are treated in the same manner as similar faults in conventional transformers. It is impractical to apply differential relays to rectifier transformers by reason of the complexity of their secondary windings and the peculiar character of the currents flowing in these windings. Therefore, the best available protective relay for this purpose is the short-time-delay induction overcurrent relay with an inverse characteristic. This relay is adjusted to have just sufficient minimum time delay (approximately 0.05 second) to allow for normal transformer magnetizing inrush currents and short-duration fault currents resulting from transient disturbances in the ignitron tubes. On systems having impedance-grounded neutrals a sensitive residual overcurrent relay also is used to clear ground faults.

Normal suppression of arc-backs and "shoot-throughs" in the rectifier and inverter tubes and d-c short circuits by the converter ignition and grid-control equipment and the igniter short-circuiting device has been discussed in a companion paper[1] describing that equipment. Operating experience so far has developed no failure of the fault-suppression equipment to clear arc-backs, "shoot-throughs" and d-c short circuits. However, such a possibility, though considered very remote, must be guarded against by backup protective relays which will prevent damage to the apparatus under such circumstances.

Rectifier arc-backs which are not cleared by the normal fault-suppression apparatus will result in sustained overcurrent in the rectifier transformer secondary winding of sufficient magnitude to cause the transformer protective overcurrent relays in this circuit to operate.

Inverter "shoot-throughs" and d-c short circuits which are not cleared by the normal fault-suppression apparatus will result in sustained overcurrents in the rectifier transformer and will produce alternating overcurrents in the lines between the rectifier tubes and the transformer; sensitive and effective protection is obtained by the use of short-time induction overcurrent relays connected to current transformers in these lines.

In the case of repeated arc-backs or "shoot-throughs," the fault-suppression apparatus merely will suppress these faults as fast as they occur, and some means must be provided for disconnecting the converter when such faulting occurs. Since each fault suppression is accompanied by a transient operation of an igniter short-circuiting device, impulses from the operation of this device are used to actuate a relay which operates to trip the power circuit breakers if the impulses occur too rapidly for predetermined time. The relay used for this purpose is a combined time-delay and notching device which, when subjected to more than a predetermined number of impulses within a predetermined period of time, closes its contacts to trip the power circuit breakers.

Short circuits on the supply system tend to reduce the voltage of that system. This reduced voltage applied to the rectifier tends to decrease the normal flow of power from the supply system to the receiving system. Since the inverter cannot function as a rectifier or the rectifier as an inverter unless the grid controls purposely are changed to give such operation, the converter cannot transfer power from the receiving system into the supply system.

Short circuits on the receiving system if of such a nature that they do not reduce the voltage of that system below approximately 80 per cent of normal will cause the converter to deliver only a nominal increase in power to the receiving system, but never more than the upper limit of power transfer provided for in the converter control.

Short circuits on the receiving system of sufficiently low impedance and on any or all phases will cause the inverter to cease to commutate the direct current. This in effect places a short circuit on the d-c system. The rectifier control immediately limits this direct current to normal maximum load current, but the inverter immediately ceases to deliver power to the receiving system. As soon as the fault on the receiving system is cleared and the voltage returns to normal, the inverter begins to commutate again and delivers normal power to the receiving system.

As previously noted, low voltage, either single- or three-phase, on the supply system merely decreases the normal flow of power or, if it is too low, causes the rectifier to cease firing, thereby interrupting the flow of power. Low voltage, either single- or three-phase, on the receiving system either causes a nominal increase in power flow or causes the inverter to cease commutation, thereby interrupting power flow.

It becomes evident, therefore, that only in case of prolonged low voltage on the receiving system, sufficient to cause a cessation of inverter commutation, is it necessary to disconnect the converter from the system. This is accomplished readily by means of a single- or three-phase time-delay undervoltage relay set to drop out at approximately 80 per cent of normal voltage and having sufficient time delay to permit system relaying to remove receiving-system short circuits.

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Differential Generator Control Relay for D-C Aircraft Electric Systems

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Synopsis: The definite-pickup type of generator control relay has been in use for a period of years on d-c aircraft electrical systems. While this type of generator control relay gives satisfactory operation when the system is in proper adjustment, it had disadvantages which are accentuated by improper adjustments of regulators and the relays. A differential type of generator control relay not subject to chatter has been developed and tested in flight on a system having four main-engine-driven generators and an auxiliary power plant. This type of relay has been shown practical and advantageous when used on a system having voltage regulators with equalizers.

Functions of Generator Control Relay

THE principal function of a generator control relay on aircraft d-c electrical systems is to connect the generator to the ship's d-c bus whenever the generator is in a condition to furnish power to the bus, and to disconnect the generator from the system whenever the generator draws reverse current from the bus above a predetermined value. The relay also provides a means of disconnecting or holding off any generator at the will of the operator.

The Definite-Voltage-Pickup Type of Relay

In the past on 28.5-volt d-c electrical systems, a relay of the voltage-pickup type has been in common use. Such a scheme is illustrated schematically in Figure 1.

The voltage-pickup type of generator control relay consists of a main contactor energized from the generator ter-

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The tests referred to in this paper and oscillograms were taken in flight by R. H. Kauffman and C. W. Hanson of the General Electric Company.

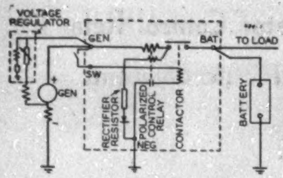


Figure 1. Schematic diagram of voltage-pickup type of generator control relay

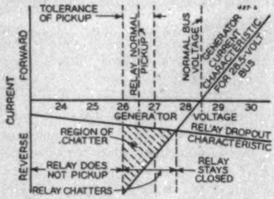


Figure 2. Characteristics of definite-voltage-pickup type of generator control relay and system

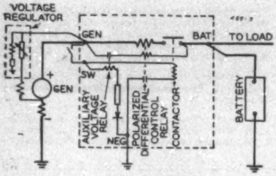


Figure 3. Schematic connections of differential generator control relay

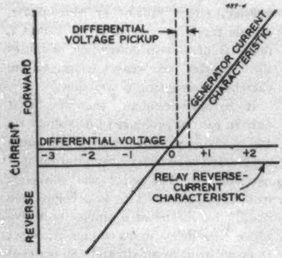


Figure 4. Characteristics of differential generator control relay and system

minal voltage through contacts on a polarized control relay having a voltage coil and a current coil. The characteristics of relays of this type are a definite voltage pickup and a dropout on reverse current which is a function of the voltage on the voltage coil, that is, it requires a higher value of reverse current to drop the relay out at 28.5 volts than at a lower voltage such as 27.0 volts. Typical characteristics of such a relay are shown in Figure 2, together with the system current characteristic for an assumed bus voltage of 28.5 volts established by other generators already on the bus. By reference to this figure, it can be seen that there is a shaded region above the pickup voltage of the relay where the relay will pick up and immediately upon closure draw a reverse current sufficient to drop it out and result in a chattering condition so long as the generator operates at a voltage in this region.

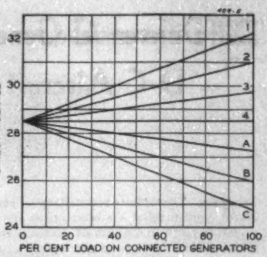


Figure 5. Characteristics of generator voltage regulators with equalizers on four-generator system

Curves 1, 2, 3—Characteristics of regulators on machines off the bus

Curves *A*, *B*, *C*—Characteristics of regulators on connected machines

Curve 4—Characteristics of regulators with all machines on

Curves 1 and *A*—One machine off, three machines on

Curves 2 and *B*—Two machines off, two machines on

Curves 3 and *C*—Three machines off, one machine on

Each time a generator comes up in speed, it operates in this region and the relay will chatter unless the generator speed is brought up so rapidly that the time duration of operation in this region is so short that the chatter does not occur. A second reason why a generator may operate in this region is in case the system load is light and its voltage regulator is set low in voltage with respect to the setting of voltage regulators on other generators operating in parallel. This is the most dangerous type of chatter because it is more likely to persist for long periods of time. It has been a real source of trouble on present systems in military service as it may result in hundreds of thousands of operations of the relay in a few hours giving rise to rapid wear. The chatter may be minimized or eliminated by raising the pickup voltage, increasing the reverse current characteristic of the relay, and by making sure that voltage regulator settings and connections are proper. If these things are done, the system is practical.

The Differential Type of Generator Control Relay

A second type of relay operating on a different principle has been developed which has several advantages which will become apparent as its characteristics are examined. This is known as a differential type of generator control relay and is the chief subject of this paper. Typical connections of such a relay are shown in Figure 3. Referring to Figure 3, the differential generator control relay consists of a main contactor energized

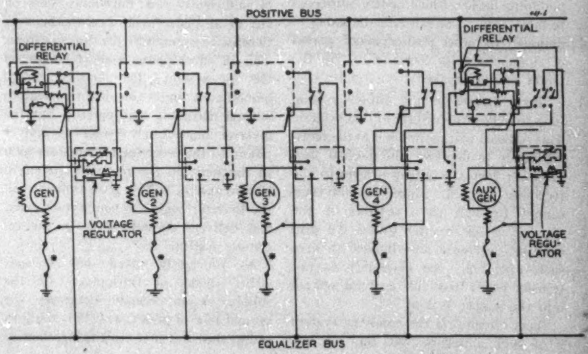


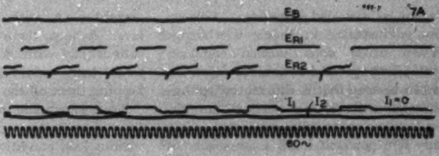
Figure 6. Schematic connections of 28.5-volt d-c system with four main generators and one auxiliary power plant

* 0.5-volt drop in negative lead for 100 per cent rated current of generator

through a contact on the differential control relay and a manual switch from the generator voltage. The differential control relay is a polarized device having a series coil and a voltage coil. The voltage coil is connected across the contacts of the main contactor through a contact on a small polarized voltage relay. The function of this latter voltage relay is to protect the differential voltage coil for large differential voltages and to prevent drain on the batteries in case the battery switches are left closed when the plane is not in operation. The characteristics of the differential relay are that it will close whenever the voltage of the generator is above the bus voltage by from 0.2 to 0.5 volt and it will drop out at a definite value of reverse current independent of the voltage of the generator. These characteristics are shown in Figure 4, together with the system current characteristic. It can be seen from this figure that there is no region of chatter because the relay will not close the generator on the bus under any condition where it will cause flow of reverse current. Likewise the relay will not automatically remove the generator from the bus on reverse current under a voltage condition that would cause it to reclose.

It might be argued that such operation could be obtained with a voltage pickup type of relay if the pickup voltage were raised to a point equal to or above the bus voltage. This is true but is not easy of attainment because of the necessary tolerance of pickup. On the differential type of relay a given percentage tolerance in the differential voltage results in only a small difference in generator voltage whereas the same percentage tolerance on the voltage-pickup type would result in a much wider variation in the actual pickup voltage. The differential type has another advantage in that the pickup voltage is related to the bus voltage so that if the bus voltage is low, the generators can come on at a lower speed and thus charge low batteries at a generator speed that could not generate enough voltage of cause a definite-voltage pickup type of relay to operate.

E_B = Bus voltage
E_{R1} = Contactor-coil voltage, relay 1
E_{R2} = Contactor-coil voltage, relay 2
I_1 = Current through relay 1
I_2 = Current through relay 2



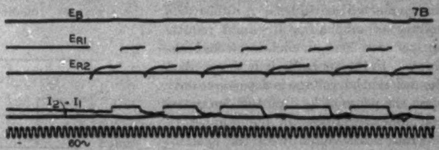


Figure 7A (upper). Oscillogram showing action at pickup on voltage-pickup type of generator control relay, taken by slowly increasing voltage setting of incoming generator regulator

Figure 7B (lower). Oscillogram showing action at dropout on voltage-pickup type of generator control relay, taken by slowly decreasing voltage of generator regulator

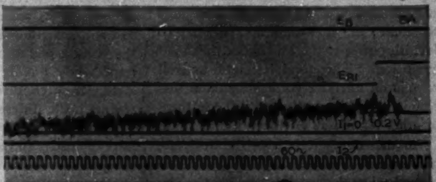


Figure 8A (upper). Oscillogram showing action at pickup on differential type of generator control relay, taken by slowly increasing voltage setting of incoming generator regulator

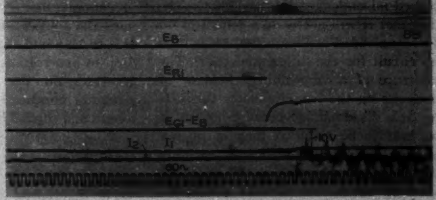


Figure 8B (lower). Oscillogram showing action at dropout on differential type of generator control relay, taken by slowly decreasing voltage of generator regulator

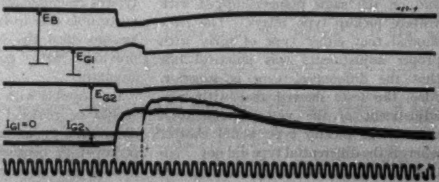
E_B = Bus voltage
E_{R1} = Contactor-coil voltage, relay 1
$E_{G1}-E_B$ = Differential voltage
I_1 = Current through relay 1
I_2 = Current through relay 2

Effect of Equalizers on Voltage Regulators in Supplementing Differential Generator Control Relays

It is to be noted that the differential type of generator control relay will close the generator on only when the generator voltage is of correct polarity and above the bus voltage. This brings up a question on a system where generators are operating in parallel with all voltage regulators set at the same voltage as to whether the first generator to come on the bus will not raise the bus voltage to a point where the other generator control relays cannot close their generators on. Were it not for the presence of equalizing windings on the voltage regulators, this might be a valid objection. However, with equalizer windings on the regulators, any load on the connected generators will not only cause the voltage of the connected generators to droop with an increase in load but will also cause the voltage of the generators not on the bus

Figure 9. Oscillogram showing pickup of one differential relay upon application of heavy (turret) load. Relay previously held off by low setting of voltage regulator

E_B = Bus voltage
E_{G1} = Voltage, generator 1
E_{G2} = Voltage, generator 2
I_{G1} = Current, generator 1
I_{G2} = Current, generator 2



to rise as the load increases. This action is illustrated in Figure 5 which is drawn based upon normal equalizer strength as specified for military aircraft. It can be seen that a differential voltage of one-fourth volt is obtained on the generators yet to be connected when only five per cent load is on each of the connected machines. A differential voltage of one-half volt would require ten per cent load on each connected machine. In the appendix it is shown that the differential voltage is dependent only upon the per cent load on the connected machines, regardless of the number of generators associated with the system. These figures are based upon all voltage regulators being set alike. However, it can be said that with the differential relay set to close on at from 0.2 to 0.5 volt on a system of generators having equalizers on the voltage regulators of normal strength, if any generator fails to come on, it would not be able to furnish power to the bus under that load condition. Therefore, it might as well remain off until its voltage regulator setting is brought into line with the settings of other regulators on the system.

Another characteristic of the differential type of generator control relay has caused misunderstanding and that is the fact that it will not close a generator on to an unloaded bus because there is then no circuit for the differential coil. The existence of a very minute load or of batteries on the bus eliminates this. It is not a disadvantage because if there is no load to be supplied there is no use putting voltage on the bus. In testing these relays on a bench test, this has sometimes caused confusion where testing the relay against an open bus without batteries or load. Only a fraction of an ampere load is required to complete this circuit.

Flight Tests With Differential Generator Control Relays

In order to prove out these principles and to satisfy any who might be skeptical of the ability to close on, tests were made on a four-engined B-24 Liberator bombing plane equipped with differential relays and on the same plane equipped with voltage pickup type relays. The tests showed that either type of relay with proper adjustments was practical but that the differential type is superior. Also, the tests showed that with misadjustment of the voltage regulators, the voltage pickup type would chatter whereas the differential type did not. No trouble was encountered in bringing any generator on with differential-type relays. In fact, those who made the tests stated that with normal regulator settings, there would have been difficulty keeping them off the bus.

Tests were made and oscillograms taken for various settings of the regulators and various load conditions. Figure 6 shows the system elementary connections.

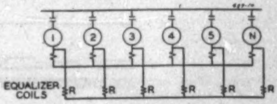


Figure 10. Schematic connection of equalizers on an *N*-machine system

These tests were purposely made severe by setting one regulator low and gradually raising its voltage setting. Figures 7 and 8 are oscillograms of these tests for the voltage-pickup and differential types, respectively. Note that Figure 7 shows a chattering condition whereas the same test with the differential relay, Figure 8, shows no chatter. Figure 9 shows a heavy load suddenly applied using differential relays with regulators set to prevent number 1 generator coming on until the application of the sudden load. It is to be noted that there was a sudden drop in bus voltage and a sudden rise in generator number 1 voltage and that generator number 1 came on within 0.05 second and carried its share of the load.

Conclusions

The differential-type generator control relay is superior to the voltage pickup type because it can be readily made to have a characteristic not subject to chatter even though the voltage regulators on generators operating in parallel do not have the same adjustment.

The voltage-pickup type of generator control relay can be made to function satisfactorily but is subject to chatter when regulators are not in adjustment on generators operating in parallel.

On a system including several generators operating in parallel, the differential type of generator control relay should give no trouble due to failure to close on the bus provided the voltage regulators are provided with equalizers of normal strength.

Less trouble with adjustment of pickup should be realized in service with the differential type than with the voltage-pickup type because a fairly wide tolerance in pickup differential voltage has a relatively minor effect on operation and will not result in chatter.

Appendix

It can be shown that for a given equalizer strength the differential voltage between machines on the bus and off the bus with all equalizer circuits closed is dependent only upon the percentage load on each connected machine. This is on the assumption that all equalizers and their leads have the same resistance and that all regulators hold constant voltage and are set for the same value except as influenced by the equalizers. Consider an *N*-machine system; see Figure 10.

The voltage applied to the equalizer circuit is determined by the load on the connected machines and the standard value is 0.5 volt for 100 per cent load. The effect of a given voltage applied to each equalizer coil is standardized. The United States Army value is a change of 2.5 volts for 0.25 volt applied to the equalizer coil of one regulator.

Let

R = resistance of equalizer coil and connected leads
N_t = number of machines on the bus
N_o = number of machines off the bus
L_t = per cent load current on each connected machine

Current in each equalizer of regulator with machine on the bus will be

$$\frac{L_t}{100}\times 0.5\times\frac{1}{N_t\left(\frac{R}{N_t}+\frac{R}{N_o}\right)}$$

Recalibration of each regulator with machine on the bus due to equalizer current is obtained by proportion as follows.

$$\frac{\frac{0.25}{R}}{\frac{L_t}{100}\times 0.5\times\frac{1}{N_t\left(\frac{R}{N_t}+\frac{R}{N_o}\right)}}=\frac{2.5}{\text{Volts recalibration}}$$

Volts recalibration below normal

$$=5\frac{L_t}{100}\frac{\frac{1}{N_t}}{\frac{1}{N_t}+\frac{1}{N_o}}$$

Current in each equalizer of regulators off the bus will be

$$\frac{L_t}{100}\times 0.5\times\frac{1}{N_o\left(\frac{R}{N_t}+\frac{R}{N_o}\right)}$$

Recalibration of each regulator with machine off the bus is obtained by proportion

$$\frac{\frac{0.25}{R}}{\frac{L_t}{100}\times 0.5\times\frac{1}{N_o\left(\frac{R}{N_t}+\frac{R}{N_o}\right)}}=\frac{2.5}{\text{Volts recalibration}}$$

]

Lighting Required for Commercial-Airline Aircraft

W. A. PETRASEK
ASSOCIATE AIEE

LIGHTING plays as essential a role in the performance of a modern air-transport airplane as it traditionally does on the stage. It is the purpose of this paper to outline the extent of the subject and to acquaint the reader with the basic requirements of each of the specific applications. Actual airline operating experience will form the basis for these requirements.

The variety of problems involved in the lighting of a commercial airliner can be readily appreciated by referring to Figure 1. The percentage of the aircraft electric power supply which is required for the lighting of two representative sizes of aircraft is shown on Figure 2.

Exterior Lighting

Position Lights

In the application of lighting for navigation purposes, it is necessary that all aircraft conform to a standard arrangement so that there will be no confusion, with its unfortunate consequences. The Civil Aeronautics Administration has specified in their regulation CAR 15.20, *Position Lights*, the particular arrangement which an air carrier must employ. This system of position lighting is shown in Figures 3, 4, and 5.

With the advent of the war and increased aerial traffic, commercial-airline position lighting has undergone critical review. This survey in which the CAA, the Bureau of Standards, and the airlines collaborated consisted of installing additional lights on an airplane and flight testing. The flight tests were so conducted as to cover all the possibilities which might be encountered in air traffic. The most exacting condition of the flight test proved to be the one in which one airplane descended upon another ship flying against a background of multicolored city lights. The results of these tests indicated that a decided improvement could be gained by flashing the position lights in a uniform sequence. The immediate outcome of these tests was an amendment to the CAA regulations requiring all commercial aircraft to flash a red and white tail light alternately at a prescribed rate of 40 cycles a minute, each cycle to have the following characteristic: 150 degrees white, 10 degrees dark, 150 degrees red, 50 degrees dark. Both the red and white lights are to be fitted with 32-candlepower lamps. These red and white lights must conform in all other respects to the previous Civil Air Regulation. Permission was granted the airlines to flash all their position lights for a trial period. Figures 6 and 7 show the type of dual tail light and wing tip installations in use on a commercial airliner.

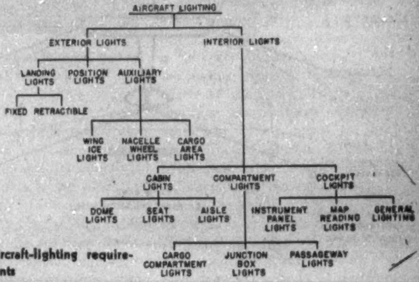


Figure 1. Airline aircraft-lighting requirements

The airlines do not regard the present system of flashing lights to be the ultimate in position lighting and are acutely aware of the need for additional study of the problem. The following remarks are intended as a guide to this further study.

The existing CAR cutoff requirements effectively limit the maximum intensity of forward position lighting. If the intensity of the position light or group of lights as viewed from another airplane is considered to be a function of their relative approach velocity, then it is obvious that the maximum light intensity should be obtained when viewing the airplane from a head-on position, and this intensity should decrease in a reasonably uniform manner to an acceptable minimum when viewing the airplane from the rear. The problem of increasing the forward position-light intensity is complicated by the undesirable effect which any bright light located within the pilot's field of vision has upon his ability to discern external objects when flying through haze, light snow, or similar weather. In fact, the pilot need not be looking out of the airplane to be bothered by the flashing of a light reflecting from the haze.

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Volts recalibration above normal

$$=5\frac{L_t}{100}\times\frac{\frac{1}{N_e}}{\frac{1}{N_t}+\frac{1}{N_e}}$$

The sum of these two recalibrations is the differential voltage which is

$$\text{Differential voltage}=\frac{5\times L_t}{100}=0.05L_t$$

Thus for differential voltage of 0.2

$$L_t=\frac{0.2}{0.05}=4\text{ per cent load}$$

For differential voltage of 0.5

$$L_t=\frac{0.5}{0.05}=10\text{ per cent load}$$

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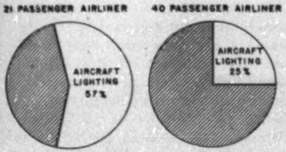

Figure 2. Relative power requirements of aircraft lighting equipment

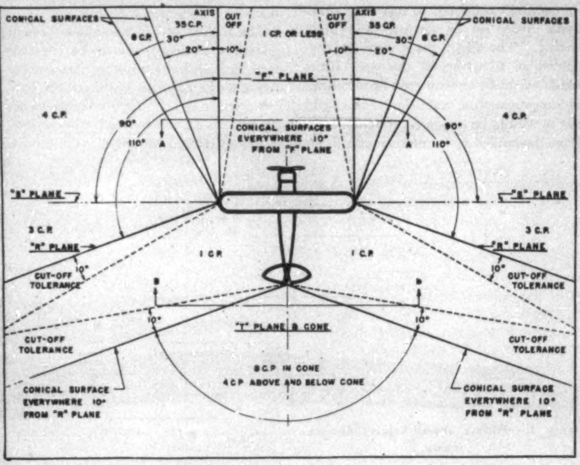
CONICAL SURFACES
AXIS
35 C.P.
CUT OFF
1 C.P. OR LESS
CUT OFF
AXIS
35 C.P.
CONICAL SURFACES
6 C.P.
30°
20°
10°
10°
20°
30°
6 C.P.
"F" PLANE
4 C.P.
90°
110°
A
CONICAL SURFACES EVERYWHERE 10° FROM "F" PLANE
A
90°
110°
4 C.P.
"S" PLANE
"S" PLANE
3 C.P.
"R" PLANE
1 C.P.
1 C.P.
3 C.P.
"R" PLANE
10°
CUT-OFF TOLERANCE
10°
CUT-OFF TOLERANCE
B
B
10°
"T" PLANE & CONE
10°
CUT-OFF TOLERANCE
CUT-OFF TOLERANCE
8 C.P. IN CONE
4 C.P. ABOVE AND BELOW CONE
CONICAL SURFACE EVERYWHERE 10° FROM "R" PLANE
CONICAL SURFACE EVERYWHERE 10° FROM "R" PLANE

Figure 3. Angles and intensities of "airline forward" and "rear" position lights

It is also necessary to consider the effect of any position-lighting arrangement upon the passengers. Here again, the annoyance of a flashing light must be minimized, and the problem is complicated by the presence of polished wing surfaces.

New navigation-light fixtures should be designed so that the streamline of the airplane will be unaffected. It is most important that this streamline be kept inviolate as ice accumulation will not only reduce the utility of the lights, but will also seriously affect the aerodynamic efficiency of the wing.

Landing Lights

The general requirement for an aircraft landing light is that it furnish sufficient illumination in the area ahead of the airplane adequately to permit the pilot to guide the airplane to a safe landing. All three portions of a landing must be considered; namely, the approach to the field, the actual landing, and the subsequent taxiing to the parking space. During these three phases of the landing, the airplane usually has three different attitudes with respect to the ground. With the fixed type of landing light which remains stationary relative to the airplane throughout the landing procedure, the illuminating effect of a beam pattern will vary with each of these phases. It is necessary that this beam pattern be so designed and the light suitably positioned that sufficient illumination is obtained on the landing strip under all conditions.

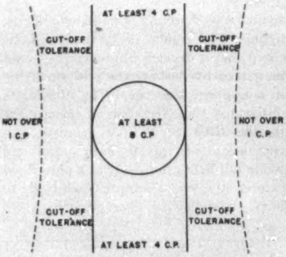
AT LEAST 4 C.P.
CUT-OFF TOLERANCE
CUT-OFF TOLERANCE
NOT OVER 1 C.P.
AT LEAST 8 C.P.
NOT OVER 1 C.P.
CUT-OFF TOLERANCE
CUT-OFF TOLERANCE
AT LEAST 4 C.P.

Figure 4. Showing section *B-B* of Figure 3

There are in general three types of landing-light installations in use. The leading-edge-of-the-wing installation is the type most commonly used in commercial-airline aircraft (Figure 8). There are several disadvantages of this type of installation. The light lost by refraction through the curved lens is excessive, and the aerodynamic efficiency of the wing is affected. Furthermore, there is interference with the deicing of the leading edge of the wings. This type of installation is susceptible to damage from impact with objects such as birds with subsequent undesirable aerodynamic effects upon the airplane. The provision for bulb replacement is complicated by the necessity for installing sufficient fasteners on the handhole cover plates to meet the structural requirements for that portion of the airplane just aft of the leading edge of the wing. Consequently, the time required for a change of bulb is excessive.

The installation of the landing lights in the nose of the airplane, while not having the objections of interference with the deicing function or aerodynamic efficiency, does have a more objectionable effect of illuminating any haze or fog to such an extent as to interfere seriously with the pilot's vision from the cockpit.

A more recent type of landing-light installation is the retractable landing light (Figure 9). This light is usually mounted well under the wing and is controlled either hydraulically or by an electric motor. The bulb can either be illuminated automatically by means of a switch incorporated in the light assembly or can be illuminated in any position from the cockpit. The military services have standardized on the general requirements for electrically retractable landing lights for 12- and 24-volt d-c airplanes. The bulb is the sealed-beam type and is readily replaceable without the necessity of removing any wing structure. It is evident that this type of light does not have the objections common to the leading-edge and nose types; however, it has the objection of added mechanical complexity. This additional mechanism is another source of trouble, and should it fail to function satisfactorily, it will render the landing light inoperative. The Civil Aeronautics Administration, recognizing the various forms in which the landing-light installations might appear, has framed its requirements as follows.

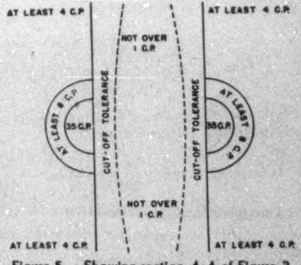
AT LEAST 4 C.P.
NOT OVER 1 C.P.
AT LEAST 4 C.P.
AT LEAST 8 C.P.
35 C.P.
CUT-OFF TOLERANCE
CUT-OFF TOLERANCE
35 C.P.
AT LEAST 8 C.P.
NOT OVER 1 C.P.
AT LEAST 4 C.P.
AT LEAST 4 C.P.

Figure 5. Showing section *A-A* of Figure 3

"Electric landing lights shall be so installed on multiengine aircraft that at least one should not be less than ten feet to the right or left of the first pilot's seat and be beyond the swept disk of the outermost propeller. On single-engine aircraft such lights shall be so installed that no visible portion of the swept disk of the propeller, if of the tractor type, is illuminated thereby. Individual switches for each light shall be provided in the pilots' compartment." It is a further CAA requirement that two lights be provided on any air carrier of passengers licensed for night flying.

To sum up the landing-light situation, present-day airplanes having the conventional tail-wheel type of landing gear do not provide suitable illumination for all three phases of airplane maneuvering involved in the landing. As a result the lights are adjusted so as to favor the illumination of the landing strip during the approach and landing position with the result that the light available for taxiing is very poor. It has been suggested that landing lights be furnished with double filaments similar to the type employed in automobiles so that taxiing might be accomplished under more favorable lighting conditions. Another solution to this problem would be properly to proportion the beam pattern from the landing light so that a sufficient amount of light is available at the various attitudes of the airplane. This can only be done, however, at the expense of the light intensity at any one working condition. In view of the compromises currently being accepted in regard to bulb life and lumens per watt output, it would be undesirable to adopt any system which does not fully utilize the lumen output of the lamps.

A problem of aircraft lighting which is seldom appreciated by anyone but the airline operator is the lamp burnout and replacement problem. This problem is particularly acute for the position and landing lights for the following reasons. An air carrier for passengers cannot be given flight clearance without the full compliment of position and landing lights as specified by the CAA. Landing lights, which are lighted just prior to the aircraft's departure from the loading ramp, and in some cases not until the aircraft is ready for take-off, usually fail when the lamp is switched on. In the case of both the position lights and the landing lights the transition, from the airplane's condition at rest with engines not running to the excessive vibration experienced when engines are being ground run and the airplane is taxiing, is sufficient to produce failure in a bulb which has approached the end of its service life. In most cases, therefore, the light-bulb failures occur at the most critical time and result in what is known as a station delay or an interrupted flight. These delays or interrupted flights are the "headaches" of airline operation.

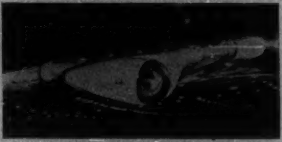
Figure 7. Installation of approved type of wing-tip position light

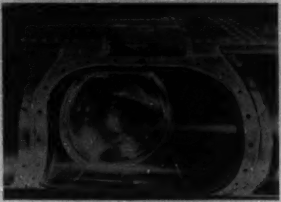
Figure 8. Leading-edge-of-the-wing type of landing light

Considering the life expectancy characteristic of the incandescent lamp, the best that one can hope for, if a periodic replacement program is put into effect, is to reduce percentagewise the number of delays and interruptions. This procedure can never eliminate them. The economy of such a program must be balanced against the intangible cost of a delay or interrupted flight. A possible solution of this difficulty for the position lights is the use of lamps having double filaments of different structural characteristics so as to provide a lamp for which only one filament would burn out at a time. This would have the advantage of providing a light of reduced intensity in case the burnout of one of the filaments occurred in flight. It is obvious that such a system could not be applied to the landing-light bulb.

Auxiliary Lights

In addition to the position and landing lights various other auxiliary lights for lighting some portion of the aircraft exterior are being used. The pilot's desire to see the leading edge of the wing at night during icing conditions has resulted in the installation of lights in either the fuselage or the outboard nacelles which floodlight the leading edges of the wings. Lights which are being used for this purpose are the type manufactured as automobile spotlights. These lights are provided with refracting lenses to provide the particular light pattern required for even distribution along the entire length of the wing. The installation of these lights has met with the approval of the CAA. They are satisfying a need which will exist until more positive and automatic wing deicing equipment is available. It is interesting to note that these wing ice lights are also used as an aid to taxiing on the ground.

Lights have been installed inside of the engine nacelles which house the landing gear in order to floodlight the gear so that the pilot may check its position in flight at night. The installation of lights adjacent to the cargo doors has been proposed so as to provide sufficient illumination for cargo handling.

Interior Lighting

Cabin Lighting

The problem of cabin lighting in a commercial airliner is quite similar to that of

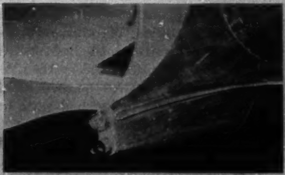
Figure 6. Dual flashing taillight installation

Figure 9. Under-the-wing-type hydraulically retractable landing light

Figure 10. Cabin dome-light installation in a DC-3 airplane

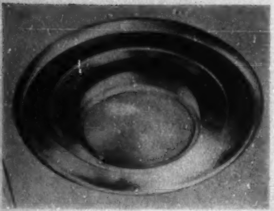

Figure 11. Combination dome light and ventilator outlet—Douglas *DC-4* airplane

the modern deluxe coach or railway car. Generally speaking, it is necessary to provide two types of illumination; namely general lighting and individual-seat lighting. The general illumination for cabins commonly consists of a number of dome-light fixtures extending along the center of the cabin ceiling and suitably spaced to provide a uniform intensity of illumination at the seat level (Figure 10). These dome-like fixtures usually consist of one or two incandescent light bulbs of 21 to 32 candlepower, each covered by a diffusing light cover made of glass or a lighter plastic equivalent. Care must be taken in the selection of the plastic compound for this purpose inasmuch as the limited space provided for the bulbs results in fairly high temperatures. The usual thermoplastic materials will sag and warp and eventually present a very unattractive appearance. Figure 11 illustrates a Douglas Aircraft Company design which places the light inside of a ventilation duct thereby obtaining better cooling of the lamp enclosure.

Some means must be provided for lowering the intensity of the dome lights during night flight so that passengers desiring to sleep will not be annoyed. The dimmed lights must provide enough illumination for the stewardess or passenger who may desire to move about the airplane. A better solution of this problem is the installation of aisle lights which may be turned on when the dome lights are extinguished. In this manner the illumination is applied where it is needed with a minimum of discomfort to reclining passengers. The aisle lights are commonly installed at the base of the individual seats and so positioned that a uniform lighting of the aisle is obtained.

A particular problem of cabin lighting at night exists at the vicinity of the main cabin door where passengers leaving or boarding the airplane must be checked in and out by the stewardess. Since it is not customary to wall off that section of the cabin containing the seats from the entrance door, the light in the vicinity of the door must be so designed that its cutoff in the forward direction will not permit it to strike the rearmost reclining-passengers' heads. Furthermore, the brightness of the light-cover glass must be kept to a minimum when observed from the rearmost seat. While it is obvious that these requirements may be simply satisfied by some sort of shade, the height of the cabin ceiling and the undesirability of installing any projecting surfaces make this method of solution impractical.

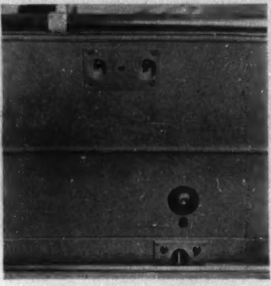

Figure 12. Individual seat light installations—Douglas *DC-3* airplane

The basic requirement of the individual seat light is that it illuminate the normal reading area in front of the passenger without annoyance to any other passengers. The normal positions and movements of adjacent passengers should not interfere with the light supplied to the seat for which it is intended. Individual-seat-light installations usually take the form of an incandescent lamp totally enclosed in a metal box with the exception of a small opening suitably designed to provide the required beam pattern (Figure 12). The switches for controlling these lights must be easily reached from their respective passenger seats. The use of lenses for the purpose of improving the efficiency of the lighting system is impractical from the maintenance viewpoint inasmuch as the type and focal length necessary for this application requires the maintenance of close tolerances between these two parts. Furthermore, the use of lenses presents an additional cleaning problem.

The individual lighting installation is further complicated on airplanes with sleeping accommodations inasmuch as suitable lighting must be provided in the upper berths. The problem of interference is minimized inasmuch as the individual compartments are partitioned or curtained; however, care must be taken that upper and lower berth lights in the same compartment do not provide stray illumination to one another.

In addition to the general cabin lighting, there are various compartments requiring lighting; namely, dressing rooms, toilets, coat closets, and buffets. Each of these compartments presents its particular problems. The general type of dome light is usually employed with the exception of the dressing rooms where additional lights are provided adjacent to the mirrors (Figure 13).

Up to the present time fluorescent cabin lighting has not made its appearance in the commercial airplane. In view of the above requirements, it appears that an application exists for the fluorescent type of lighting for supplying general cabin and compartment illumination. In the application of fluorescent lighting to aircraft, the following factors must be evaluated:

1. What will be the difference in weight over that of an incandescent bulb type of installation providing the same level of illumination?
2. How will the service life and cost of operation compare with the existing system of incandescent lighting?
3. Does the improved appearance of the fluorescent type of installation warrant penalties to be taken on either the weight or maintenance items?

COCKPIT LIGHTING

If one were to ask what the most important lighting application is in an airplane, the answer would be universal in its agreement on the instrument panel lighting. The importance of proper panel lighting and its contribution to the safe operation of the airplane has long been recognized and a number of different types of lighting systems developed.

The general problem of instrument-panel lighting can be stated as the application of a controlled, uniform intensity of shadowless illumination over all instrument dials which must be observed by the pilot during the routine operation of the airplane. Furthermore, this illumination must be so accomplished that there are no extraneous reflections of light visible to the pilots from instrument cover glasses, windshields, or other reflecting surfaces. The instrument-lights' function has been similarly described by the CAA in their regulation CAR 04.5826 which reads, "Instrument lights shall be so installed as to provide sufficient illumination to make all flight instruments easily readable and shall be equipped with rheostat control for diming unless it can be shown that a nondimming light is satisfactory."

There are in general three types of instrument-light installations. They are:

1. Incandescent bulb floodlighting of the instrument panel.
2. Individual (built in) ring lights on each instrument.
3. Fluorescent floodlighting of the instrument panel.

Each of the above methods has its advantages and disadvantages of which the following are the more important. In floodlighting the panel by incandescent light bulbs, the following problems are outstanding:

1. Reflections from instrument cover glasses and windshields.
2. Even distribution and shadowless illumination of all the instruments.
3. The difficulty of locating the various lighting fixtures in the airplane to accomplish the first two objects.

The individual ring lighting of instruments solves the problem of extraneous reflections and of securing uniform and shadowless illumination. However, the chief disadvantage of this type of lighting is the penalty which must be taken in the installation as each instrument must have a wire connection, and these wires must be located in back of the instrument panel which is one of the more congested spots on the airplane.

The use of fluorescent lights for instrument lighting has been given considerable impetus by the military services for whom the dark-cockpit type of night flying is an absolute necessity because of tactical reasons. The principle of fluorescent lighting has been widely demonstrated to the general public by theaters which have used this medium for staging spectacular costume changes. It is based upon the action of near-ultraviolet radiation on fluorescent paints. The wave length of this radiation is in the band from 3,000 to 4,000 angstroms and consequently has no detrimental effect to the eyes or skin. The efficiency of the fluorescent lamp is approximately two to three times that of the equivalent wattage of incandescent lamp. The history of the instrument-panel fluorescent lighting goes back to the year 1934 when an installation of a cold-cathode argon lamp was used with a cobalt-blue glass filter to flood the instrument panel with near-ultraviolet radiation (Figure 14). The instruments were coated with a radium base paint which reacted quite readily to these lights and produced a legible instrument indication without any extraneous illumination. The limitations of the argon lamp as a source of ultraviolet light became apparent as the size of instrument panels and the distance which these lights had to be mounted from it became greater.

A series of 4- and 6-watt 118-volt a-c fluorescent lamps were designed in conjunction with the United States Army Air Corps, who pioneered in their development. The design of these lamps is unique in that they contain a fluorescent coating on the inside of the glass envelope which transposes the ultraviolet radiation of the ionized mercury-vapor gas into both visible and near-ultraviolet radiation. The lamp is provided with a rotable shutter and a blue glass filter which permits some control in the amount of near-ultraviolet radiation output and permits the use of visible light for reading of maps and charts in the cockpit. These lamps require a power source of 118 volts for their operation as well as a reactor (ballast) and a starting switch similar to that employed in the commercial fluorescent lighting equipment.

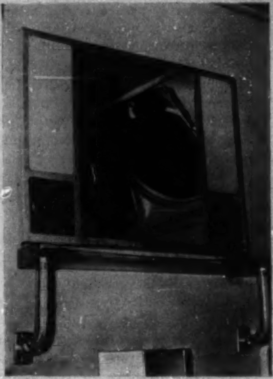

Figure 13. Mirror lights—Douglas DC-4 airplane

Within the past year a 24-volt d-c mercury-vapor bulb has been made available. The same basic principles apply with the exception of the size and shape of the bulb which resembles an automobile headlight lamp and is enclosed in a fixture provided with a slotted face for permitting either dark lighting or visible lighting of the instrument panel. The use of the 24-volt lamps in aircraft having 24-volt d-c electrical systems has abolished the need for inverters formerly required for the 118-volt equipment.

In conjunction with the design of the fluorescent lighting equipment just described, the military services have sponsored the use of fluorescent-paint materials which are activated by the near-ultraviolet radiation but which extinguish immediately upon removal of the radiation. The development of fluorescent-paint material has progressed to the stage where a wide range of colors are available which will fluoresce with approximately the same color as they appear in daylight. This property is advantageous in order to preserve the utility of the instrument limit markers used to designate various safe minimum and maximum values of engine pressures, temperatures, and airspeed.

Figure 14. Instrument-panel light installation—DC-3 airplane

Independent studies made by a number of physiologists have demonstrated that the ideal color for instrument illumination is in the red spectrum, the reason being that the red wavelengths have very little effect upon the dark-adapted eye. As a result of these findings, incandescent-light fixtures have been equipped with red glass filters for floodlighting panels with red light for night flying where a minimum of interference with the pilot's ability to discern objects through the windshield is required.

As in most lighting applications, particularly those involving colored lights, the personal element is an important factor. As an example, one can cite the attitude of the commercial airline pilots to the argon lighting system which has been in use for the past nine years. There are about as many pilots who do not use this form of lighting because they personally do not like it as there are pilots who would not be without it. The same reactions have been recorded by the physiologists who have noted the personal opinions of the various subjects testing the red-light installation. Much to the surprise of some of these subjects, the tests showed that while they disliked the colored lighting, their visual ability to see objects out of the cockpit had been improved over their previous ability with their customary and preferred type of illumination.

Some form of glareless general cockpit

illumination is necessary in addition to various spotlights provided for illuminating the pilot's controls, switches, valves, etc. It is common practice for most pilots to fly with a brightly lighted cockpit when cruising on flights of long duration. Apparently this practice has been found less fatiguing than flying with a darkened cockpit with only localized lighting for illumination. The ability to flood the cockpit with general illumination is a desirable safety measure when flying through thunderstorms in the presence of lightning. It has been determined from a number of recorded cases in which pilots were momentarily blinded by lightning flashes that the duration of their disability was generally related to the intensity of cockpit illumination prior to the discharge.

In addition to the problem of illuminating the instrument panel for the pilots, there is the problem of satisfactorily illuminating the work areas for the flight engineer, navigator, and radio man in the larger airplanes having provisions for such personnel. The lighting provisions for each of these crew members usually require the illumination of an auxiliary instrument panel mounted in front of the observer plus a local spotlight for illuminating his table area. All lighting for the individual crew member must be carefully arranged so as not to interfere with the other crew members. The various lighted work areas of the nonpiloting crew members must be shielded from the pilots' compartment and it is customary to drape a curtain between the pilots and the remainder of the flight-crew compartment. It is also necessary to provide a suitable light shield for the navigator's use when sighting through the ship's astrodome. In general, the location of the various light sources and curtains must be determined from a full-scale mock-up of the crew compartment. It is essential that the installation of all glass reflecting surfaces be duplicated in the construction of the mock-up. Only in this manner can the multiple reflections obtained from these windows be detected and eliminated.

Conclusion

The problems associated with the lighting of a commercial transport airplane are many and varied, and current practice leaves much to be desired. Generally speaking, there is need for the development of colored position-light sources of greater intensity and better efficiency. Such developments should seek a reduction in size and weight of the installation and be designed to fit more closely the aerodynamic streamline of the airplane.

In the field of aircraft landing lights the development of a light which will meet all the requirements associated with the landing and ground running of the airplane without increasing the weight of the installation is needed. The present life of the position light and particularly the landing-light bulb is exacting too great a penalty from the airline operator in view of the coincidence of lamp failures with the time of aircraft departure.

Perhaps the greatest immediate change which can be expected in the post war commercial airliner will be in the realm of cabin lighting. This opinion is based upon the unexploited application of fluorescent lighting to aircraft cabin interiors. Light installations of more pleasing appearance and producing a higher intensity of illumination for the individual passenger at less cost in weight and energy are needed.

The flight crew personnel can look forward to improved cockpit lighting installations as a result of the experience which is constantly being accrued by the military services in the laboratory as well as in the field.

No paper on aircraft lighting would be complete without due acknowledgement to those agencies which are actively engaged in furthering commercial aircraft lighting developments. These are the aircraft lighting committee of the Air Transport Association, the committee on aviation lighting of the Illuminating Engineering Society, the technical development division of the Civil Aeronautics Administration, and the United States Bureau of Standards. These agencies are mutually co-operative and will provide the necessary guiding hand to the future development of commercial aircraft lighting.

Fusing Practices on Distribution Systems—II

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A PAPER on fusing practices on distribution systems operating at five kilovolts and below was presented at the winter technical meeting.[1] It discussed the results of a questionnaire which had been submitted to about 200 operating companies and presented the data in graphical form. The data of the present paper were gathered on systems operating between 5 and 15 kv. Wherever possible, the form of the previous paper has been followed in order to permit ready comparison.

Distribution Transformer Fusing

In answer to the question, "Why do you fuse distribution transformers?" it was found that 86 per cent of the companies fuse to protect the system from the transformer, 61 per cent fuse to protect the transformer, 18 per cent fuse to indicate the location of faults, and 17 per cent fuse as an isolating means. The total of these percentages is more than 100 per cent, because several companies expect the fuse cutouts to perform more than one function.

The primary windings of distribution transformers are fused by the use of enclosed indicating cutouts by 34 per cent of the companies. The enclosed nonindicating cutout is preferred by 12 per cent of the companies, and 54 per cent expressed a preference for the open dropout type. Perhaps the large preference for the open type is caused by the fact that the insulation is such as to put a grounded mounting bracket between the terminals of the fuse. At this higher voltage the dropout feature is almost a necessity in order to prevent voltage from appearing across exposed organic insulation of a blown fuse.

The use of repeating fused cutouts on distribution transformers is very limited. This probably is brought about by the relatively high cost of this type of cutout and by the fact that almost all internal transformer faults are of a permanent nature. However, nine per cent of the companies reported some use of repeating cutouts for transformer fusing. This agrees with the eight per cent found on systems below five kilovolts.

Paper 44-97, recommended by the AIEE committee on protective devices for presentation at the AIEE summer technical meeting, St. Louis, Mo., June 26-30, 1944. Manuscript submitted April 19, 1944; made available for printing May 5, 1944.

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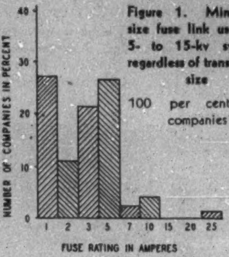


Figure 1. Minimum-size fuse link used on 5- to 15-kv systems regardless of transformer size

100 per cent=104 companies

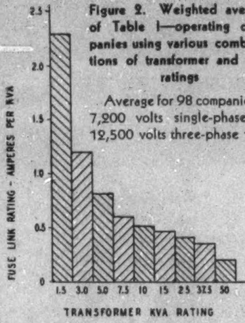


Figure 2. Weighted average of Table I—operating companies using various combinations of transformer and fuse ratings

Average for 98 companies 7,200 volts single-phase or 12,500 volts three-phase wye

As in the previous questionnaire, the companies were asked the extent of the use of cutouts at locations where the short-circuit current exceeds the published interrupting rating. It was found that the policy of 37 per cent of the companies is to use cutouts at locations where the published interrupting ratings might be exceeded.

Transformer Fuse Ratings

The rating of the primary fuse which is chosen for a distribution transformer is usually correlated with the kilovolt-ampere rating of the transformer. In the case of smaller transformers, usually below five kilovolt-amperes, other factors predominate. Consequently, the question was asked as to the minimum-size fuse link used on the system and the factors controlling its choice. The three factors most frequently mentioned in the order of their predominance were mechanical strength, lightning, and transformer rating. Figure 1 illustrates the practice as to minimum fuse rating and shows graphically the number of companies in per cent using a given size. The range of values is about the same as that found for lower-voltage systems except that a larger number use the one-ampere link.

Table I is a transformer fusing chart showing the percentage of operating companies using the various combinations of transformer and fuse ratings. The most popular link for each transformer rating has been pointed out by the bold-faced type in the table. This table applies to 6,600–7,600-volt single-phase systems and 11,000 to 13,800 volts three-phase wye systems. It is interesting to note that the plurality of customers use a five-ampere link as a minimum size for all transformers up to 15 kva. A weighted average of Table I 's graphically presented in Figure 2 which shows the fuse link rating in amperes per kilovolt-ampere plotted against the transformer kilovolt-ampere rating. It will be noted that the fusing ratio varies widely, a much higher fusing ratio being used for the smaller transformers. The data in Table I have been plotted again in Figure 3. This table shows the number of companies in per cent using a given fusing ratio, the fusing ratio being the link rating divided by the full-load current of the transformer. The data show even less uniformity in fusing practice than is found for transformers operating below five kilovolts.

Table II applies to systems operating at voltages ranging from 6,600 to 7,600 volts three-phase delta. Three-phase wye systems from 6,600 to 7,600 volts are shown in Table III. Single-phase systems operating between 11,000 and 13,800 volts are found in Table IV. Table V applies to systems operating in three-phase delta between 11,000 and 13,800

Table I. Operating Companies Using Various Combinations of Transformer and Fuse Rating

7,200 Volts Single-Phase or 12,500 Volts Three-Phase Wye
100 Per Cent=98 Companies

Single-Phase Transformer Rating—Kva (Multiply by 3 for Bank Kva)	Fuse Link Rating—Amperes										
	1	2	3	5	7	10	15	20	25	30	40
	Number of Companies in Per Cent										
1.5	22.8	17	19.3	**35.3**	1.1	4.5					
3	20.0	14.4	20.0	**40.0**	1.1	4.5					
5	5.3	8.5	31.9	**44.7**	2.1	7.5					
7.5	3.0	8.0	23.0	**46.0**	7.0	13.0					
10		7.1	28.6	**42.8**	3.6	14.3	3.6				
15		2.2	4.3	**55.4**	8.7	17.4	7.6	3.3		1.1	
25			2.1	8.3	24.7	**42.2**	11.3	6.2	3.1	2.1	
37.5				2.2	3.3	**42.4**	29.3	8.7	7.6	5.4	1.1
50					1.6	14.8	**50.8**	14.8	9.8	8.2	

Table II. Operating Companies Using Various Combinations of Transformer and Fuse Ratings

7,200 Volts Three-Phase Delta; 100 Per Cent=33 Companies

Single-Phase Transformer Rating—Kva (Multiply by 3 for Bank Kva)	Fuse Link Rating—Amperes												
	1	2	3	5	7	10	15	20	25	30	40	50	65
	Number of Companies in Per Cent												
1.5	3.0	11.5	**42.3**	34.6		7.7							
3	3.6	3.6	28.5	**53.5**	3.6	7.2							
5		3.0	18.2	**42.5**	21.2	12.1	3.0						
7.5		3.2	3.2	**35.6**	6.5	29.0	19.3	3.2					
10				16.1		**42.0**	16.1	16.1	9.7				
15				3.0	6.1	**30.3**	18.2	21.2	9.1	12.1			
25						**19.4**	16.1	12.9	12.9	12.9	12.9	12.9	
37.5							**20.0**	**20.0**	6.7	13.3	16.7	3.3	3.3

Table III. Operating Companies Using Various Combinations of Transformer and Fuse Rating

7,200 Volts Three-Phase Wye; 100 Per Cent=21 Companies

Single-Phase Transformer Rating—Kva (Multiply by 3 for Bank Kva)	Fuse Link Rating—Amperes												
	1	2	3	5	7	10	15	20	25	30	40	50	65
	Number of Companies in Per Cent												
1.5	7.1	14.3	26.6	**37.7**	7.2	7.1							
3	7.1		28.6	**57.2**	7.1								
5	6.3			**62.3**	18.9	12.5							
7.5			6.3	**50.0**	18.7	18.7	6.3						
10			6.2	**31.3**	6.2	**31.3**	25.0						
15				14.3		28.6	**42.8**	9.5	3.8				
25						**40.0**	13.3	20.0	20.0				
37.5						**26.6**	6.7	13.3	6.7	**26.6**	6.7	6.7	6.7

volts. The data of Tables II through V could be shown in graphic form as was Table I in Figures 2 and 3 and would show the same general trend.

All companies replying use primary fused cutouts. Reference to Figure 3 will show that the fusing ratios are such that in many cases low-impedance secondary faults near the transformers will be cleared by the primary fuses. However, secondary fuses are used by 18 per cent of the companies, as contrasted with 12 per cent for systems operating below five kilovolts.

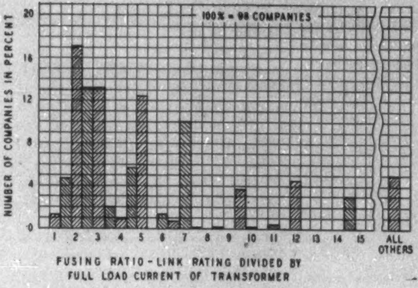


Figure 3. Weighted average fusing ratio for all ratings of transformers plotted from data of Table I

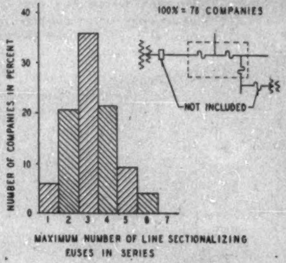
Line Sectionalizing

It was found that 88 per cent of the companies make use of fused cutouts for line sectionalizing. Of these, 66 per cent use them on the main feeders, 87 per cent on the three-phase branch feeders and 95 per cent on single-phase laterals. The repeating cutout is most frequently found on the main feeder where 67 per cent are of this type. The three-phase branches are fused by repeating cutouts in 52 per cent of the cases, and this figure drops to 39 per cent on single-phase laterals. About 74 per cent of all cutouts used for line sectionalizing are of the open type. This contrasts sharply with the practice found on systems operating below five kilovolts, where 70 per cent are of the enclosed type. The rather wide variation in fusing practice for line sectionalizing is illustrated by Figure 4. This shows the number of companies in per cent using a given number of line-sectionalizing fuses in series. This figure shows that 63 per cent of the companies consider the use of more than three sectionalizing fuses in series to be impractical.

Conclusions

In general, the data applying to systems above five kilovolts agree closely with those for systems below five kilovolts. Perhaps one reason for this is the fact that the same companies were questioned in each case. There are a few discrepancies, such as the preference for open cutouts at the higher voltage, and these are easily understood from a consideration of the factors involved. There appears to be a trend towards larger fuse sizes for use with distribution transformers, which the authors consider to be very desirable. The high percentage of the repeating fuses for sectionalizing service is indicative of a trend toward automatic reclosing, but it is the authors' belief that the small reclosing breaker eventually will supplant repeating fuses for line protection.

Figure 4 (below). Line - sectionalizing practice employed by 76 companies



Discussion with operating engineers reveals that many have come to the conclusion that the use of larger fuse links leads to reduced operating expense without any actual increase in the rate of transformer failure. The larger link while reducing unnecessary outages does not change appreciably the time required to disconnect a faulted transformer from the circuit. As no fuse link can give adequate thermal protection to a transformer, little if anything is lost by employing larger links. It is hoped that further study of this problem will lead to additional operating data which will allow quantitative interpretation of the merit of increasing the size of fuse link used on transformers.

Table IV. Operating Companies Using Various Combinations of Transformer and Fuse Rating

13,200 Volts Single-Phase; 100 Per Cent = 44 Companies

Single-Phase Transformer Rating—Kva	Fuse Link Rating—Amperes								
	1	2	3	5	7	10	15	20	25
	Number of Companies in Per Cent								
1.5	22.6	9.1	9.1	41.0		18.2			
3	33.4	12.1	9.1	30.3	3.0	12.1			
5	34.1	9.1	20.4	25.0	2.3	9.1			
7.5	9.8	24.4	19.5	34.1	2.4	9.8			
10	7.1	7.1	21.5	21.5	7.1	35.7			
15		11.4	31.8	45.4	6.8	4.6			
25		5.0	12.5	50.0	12.5	17.5		2.5	
37.5			4.9	31.7	17.1	34.2	7.3	2.4	2.4
50				9.8	7.3	48.8	24.4	7.3	2.4

Table V. Operating Companies Using Various Combinations of Transformer and Fuse Rating

13,200 Volts Three-Phase Delta; 100 Per Cent = 36 Companies

Single-Phase Transformer Rating—Kva (Multiply by 3 for Bank Kva)	Fuse Link Rating—Amperes												
	1	2	3	5	7	10	15	20	25	30	40	50	65
	Number of Companies in Per Cent												
1.5	15.4	11.5	23.1	46.2	3.8								
3	6.5	9.7	25.8	51.5	6.5								
5	3.0	6.1	21.2	45.4	18.2	6.1							
7.5		5.6	5.6	50.0	11.0	25.0			2.8				
10		3.2	3.2	25.9	16.1	19.3	25.9	3.2	3.2				
15			2.8	27.7	5.6	22.2	22.2	16.7	2.8				
25				2.9	5.9	38.3	5.9	17.6	14.7	11.8		2.9	
37.5					8.6	17.1	22.9	17.1	5.7	11.4	11.4	2.9	2.9

Reference

1. FUSING PRACTICE ON DISTRIBUTION SYSTEMS—I, John S. Parsons, J. M. Wallace. AIEE TRANSACTIONS, volume 63, 1944, March section, pages 89–91.

Electronic Regulator for Arc Furnaces

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Synopsis: With the increased use of arc furnaces in the production of alloy steel, new interest has been aroused in both old and new methods of automatic regulation of electrode position. The regulator described in this paper is an electronic type which controls electrode position as determined by response to current in the electrode and voltage between the electrode and the furnace shell. Features and performance of an installation for a furnace having hydraulic control are illustrated.

THE power input to arc furnaces has always been controlled by automatic adjustment of electrode position. Figure 1a shows a typical installation of an arc furnace which employs hydraulic electrode positioning. In this type of furnace air up-push is used to counterbalance the weight of the electrode arm. Hydraulic means, controlled by a small motor, is employed to lower or raise the arm.

In the operation of the furnace, electrode adjustment is necessary because, as the charge melts down, the electrode must be lowered to maintain the arc. The scrap, as it melts, frequently falls against the electrodes, causing a short circuit and making withdrawal necessary. Adjustment of the electrodes is also necessary to allow for normal consumption of the graphite of which they are made.

Early methods of arc-furnace control consisted of simple current-operated devices. The electrode current in each phase was supplied from a current transformer to the coil of the regulator element. The pull of the coil was then balanced against a spring and the operation of the regulator contacts caused the electrode motor contactors to close the motor armature circuit in a manner to either raise or lower the electrode as the current deviated from a predetermined value. This method of regulation was found generally unsatisfactory, chiefly because the restraining force of the spring had a constant value and other factors were not taken into account.

A better method was then developed using another coil in place of the spring. Across this coil was impressed the arc voltage. As is well known, the voltage drop across the arc will increase as the current is decreased and vice versa. It can be readily understood that, when the varying pull of the coil or other regulating device which is proportional to the electrode current is balanced against a restraining force which is proportional to the arc voltage, the utmost sensitivity to changes in arc length is obtained in the regulator.

This principle of regulation has been in use for a period of about 15 years and has proved the best so far devised. For further improvement it seemed necessary to look to the actual control of voltage impressed across the electrode control motors, which for many years has been a constant potential d-c reversing control combined with dynamic braking. Figure 1b shows a view of the electrode motors used for controlling the electrodes of the arc furnace shown in Figure 1a. The motor in this case is one-half-horsepower frame size.

Quite recently electronic motor control has been developed and has proved successful on many applications where wide range of speed control and quick response is desired. It is possible to obtain many combinations of acceleration and speed range with a minimum of apparatus and almost instantaneous response. The means for varying motor speed is different from the older method of varying the field of the motor and holding a constant potential on the armature. Instead, the field is held constant and the armature voltage is varied to give the desired speed. The use of electronic regulation and control of the type described in this paper, for electrode motors, eliminates moving parts in the electrical equipment up to the motor. At the same time it provides the necessary sensitivity and high speed of response.

The electronic regulator includes a number of electronic tubes which take the place of relays and contactors to control the input to the furnace electrode motors. The power supplied to the regulator is taken from the available a-c line and no direct voltage is required from any separate source. The grid to cathode circuit of the vacuum tubes constitutes the input of the device, while the output is obtained from the thyratron-tube circuit. The principal grid voltages for the vacuum regulating tube include:

1. A control voltage which serves as a reference against which the quantity to be regulated is compared.
2. A control voltage which is proportional to the quantity to be regulated.
3. A self-biasing voltage connected in series with the cathode.

For a high degree of accuracy, the various grid voltages should have complete control over the regulating tube without causing the tube to saturate

Figure 1a. Typical arc furnace having electrodes arranged for hydraulic positioning

Figure 1b. Electrode control motors for the furnace shown in Figure 1a

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The authors acknowledge the co-operation and contributions of C. C. Levy of the industry engineering department, Westinghouse Electric and Manufacturing Company, in the development of this regulator, and also the co-operation of the Hydro Arc Furnace Company and the Electrocast Steel Foundry for the facilities placed at their disposal in making tests.

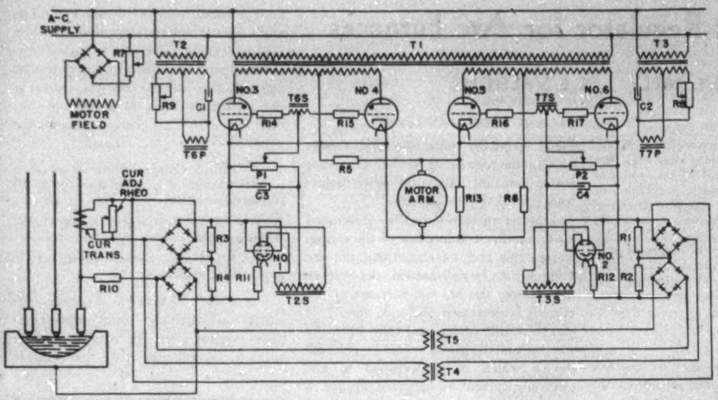

Figure 2. Simplified schematic diagram of arc-furnace regulating system

because of the grid swinging positive. The self-biasing resistor is designed so that the regulating tube will be working along the straight line of its saturation curve similar to that of a class *A* amplifier. This results in a high degree of amplification since an extremely small change of the grid voltage is ample to change the output of the regulating tube over a wide range.

The speed of response of this type of regulating element is almost instantaneous. There is practically a complete absence of any retarding quantity such as inductance which might delay changes of current flow within the element.

Figure 2 shows an elementary schematic diagram of the regulating system. To simplify the diagram, certain details including grid balancing circuits, have been omitted. The grid control voltage of the regulating tubes, proportional to the electrode current, is taken from two Rectox rectifiers, which are connected to a current transformer in the electrode circuit. The grid control voltage, proportional to the arc potential, is taken in a similar manner from the voltage between each electrode and the shell of the furnace. In practice, when the furnace transformer breaker is closed, voltage is applied to the potential circuit which causes the output of tube number 1 to decrease. This in turn decreases the drop across potentiometer *P*1, supplying negative grid direct voltage to tubes numbers 3 and 4. When the negative grid voltage is decreased, tubes numbers 3 and 4 break down and start conducting, supplying full-wave rectified voltage to the electrode motor in the direction to lower the electrodes.

When the first electrode strikes the metal, the voltage drops to zero causing the voltage to be removed from the potential circuit, thereby allowing tube number 1 to increase its output, consequently increasing the negative grid voltage on tubes numbers 4 and 5, thereby blocking the conduction of current through these tubes. When a second electrode strikes the metal, current flows causing a voltage to appear in the current circuit which will place a negative grid voltage on tube number 2, thereby decreasing its output. Tubes numbers 2, 5, and 6 and potentiometer *P*2 will operate in the same manner as tubes numbers 1, 3, and 4 and potentiometer *P*1 did for the lowering operation. A voltage will then be impressed on the armature circuit in the direction to raise the electrodes thus establishing an arc. As the arc is lengthened, its voltage increases and the current decreases until a balance is established between the potential and current grid control voltages. The third electrode is controlled in a similar manner.

As the charge melts and flows down, or as the electrodes burn off, or the scrap falls against the electrodes, the balance between the control grid voltages is continually being disturbed. When this takes place, the two regulating tubes change the voltage output from the thyratron tubes that supply the electrode-motor armature power.

The variable voltage for the motors is obtained by superimposing an alternating voltage wave on the grid direct voltage of the main thyratron tubes. By displacing the a-c wave 90 degrees lag with respect to the thyratron anode voltage, the a-c wave will cut the critical grid voltage line at different points as shown by Figure 3, as the direct grid voltage of the power tubes is either raised or lowered. This allows the thyratron to conduct for varying portions of the half cycle.

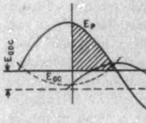

Figure 3 (below). Diagram of phase-angle firing control

A wide range of current adjustment is obtained by means of a rheostat shunting

Figure 4. Metal-enclosed electronic regulator unit

the current transformer. This method is used since it maintains the same sensitivity for all current settings.

Provision is made for controlling the electrodes manually, either individually or in a group. This facilitates handling of the electrodes during charging, pouring, and changing electrodes.

Figure 4 shows a front-view arrangement of the regulating equipment in a single metal-enclosed unit. A hinged panel door is used on the front of the regulator cabinet to permit access to various tubes and their connections. A bolted rear plate is provided in two sections so that any one or all of the regulator panels can be readily removed. The control panel is arranged for setting into the wall of the furnace vault in the usual manner.

The equipment is made so far as possible in one unit so that the various parts can be protected from dirt and physical damage. This also permits factory assembly and complete test, up to the operation of the electrode motors. A minimum of external connections and erection time is thus required.

Figure 5 is an oscillogram showing typical operation of the furnace during the melt-down period. This demonstrates the high speed of the electrode. Unsustained sudden variations in current cause no extra action of the equipment, but sustained changes cause immediate response. It will be noted that the electrode is withdrawn at relatively high speed when a cave-in takes place.

The start of the picture shows the electrode in comparatively stable operation, then suddenly the arc current is stopped indicating the arc has been broken. Instantly voltage is applied to the electrode motor to lower the electrode and re-establish the arc. Contact is made and the arc is drawn out until the arc voltage and current are returned to the desired quantities. The film shows this operation to have taken place in approximately 19 cycles. It should be appreciated that during the melt-down period, electrical conditions change rapidly, necessitating more or less continual electrode movement.

Figure 6 is an oscillogram showing performance during a portion of the refining period. Here relatively small changes in current take place and consequently only small movement of electrodes is required. Because of the inertia of the motors and electrodes, sudden-unsustained variations do not cause movement of the electrodes.

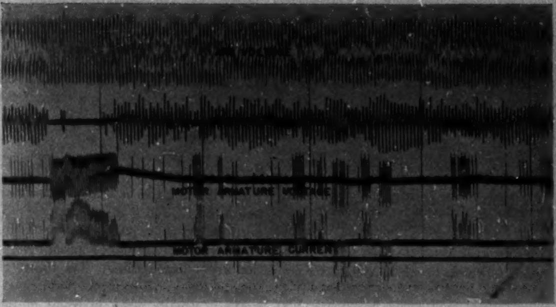

Figure 5. Oscillogram showing electrical performance of regulator during melt-down period

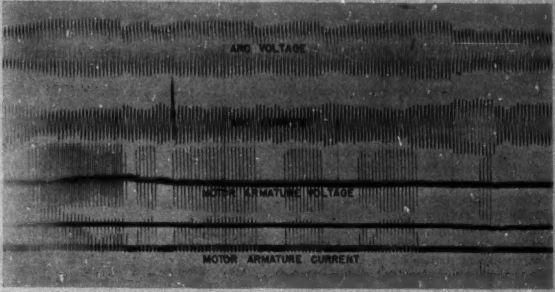

Figure 6. Oscillogram showing elecrical performance of regulator during refining period

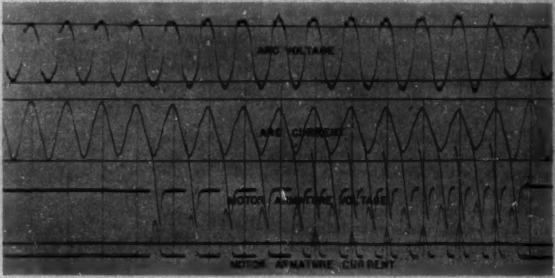

Figure 7. High-speed oscillogram showing electrical performance of regulator during refining period

Figure 7 shows a high-speed oscillogram taken during the refining period. This film shows quite accurately the high speed of response. The picture shows the start of an unbalance and in less than two cycles voltage has been impressed across the motor armature. In 11 cycles the electrode has been returned to its desired position.

Operating experience has shown that the quick response of this regulator is particularly advantageous during melt-down periods when cave-ins frequently occur. The high-speed withdrawal of electrodes prevents breaker tripping due to sustained overload.

The regulator described in this paper was designed for a furnace which requires relatively small electrode motors by reason of using counterbalanced electrodes

Formulas for the Four-Terminal Network Parameters of Uniform Ladder Networks

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Synopsis: In the treatment of the over-all behavior of a smooth transmission line, or uniform ladder network, one frequently wishes to deal with the commonly termed *A-B-C-D* parameters rather than the propagation function, to which the former are related by hyperbolic functions, and the characteristic impedances. However, the latter are the functions which are simply related to the network components. The *A-B-C-D* parameters may be given as power-series expansions of the variable α, where $\alpha^2 = Z_TY_T$ and Z_T and Y_T are the respective total series impedance and shunt admittance. For the smooth line these are infinite series; but they are terminating series for the ladder network, the number of terms and the coefficients of the terms being functions of the number (M) of elementary π or T sections of which the network is composed. Formulas for these coefficients are developed and they are given in several alternate forms. Also included are recurrence formulas and some relations among the coefficients of the different parameters. One form for the coefficients is particularly interesting because it shows that each coefficient for the ladder network is the corresponding smooth-line coefficient multiplied by a polynomial consisting of unity plus a polynomial in nonzero powers of $1/M^2$. This puts in evidence the manner in which the two coefficients approach each other as M increases; and also leads to easily computable upper and lower bounds of the ladder-network coefficients, which may provide useful estimates of their magnitudes.

The Parameters of Four-Terminal Networks

THE general theory of four-terminal networks is well known and may appear in any one of several forms, depending on the combinations of external currents and voltages which are to be related. In the present discussion we are interested in specifying the sending-end voltage and current, each in terms of linear combinations of both receiver voltage and current. In the general case of Figure 1, which may be a nonsymmetrical network, the required relations may be written

$$\left.\begin{aligned} E_s &= AE_r + B'I_r \\ I_s &= C'E_r + DI_r \end{aligned}\right\} \quad (1)$$

where the notation is that of the figure and A, B', C', and D are constants of proportionality. (The primes on B and C are used here merely to allow a uniform notation in the final results.) The theory of smooth lines and recurrent ladder networks is usually given in terms of the surge impedances (two in the case of a nonsymmetrical network) and the propagation function. However, this is an artificial point of view when one is interested in the over-all behavior of the circuit, considered as a four-terminal network, the parameters of equations 1 being more suitable. Occasion will arise in the subsequent discussion to compare the formulas for the ladder network with those for the smooth line. Therefore, we begin by putting the smooth-line parameters into forms suitable to our purpose. The following relations are well known:

$$\left.\begin{aligned} E_s &= (\cosh\alpha)E_r + \sqrt{Z_T/Y_T}(\sinh\alpha)I_r \\ I_s &= \sqrt{Y_T/Z_T}(\sinh\alpha)E_r + (\cosh\alpha)I_r \end{aligned}\right\} \quad (2)$$

where Z_T and Y_T are respectively the total series impedance and shunt admittance of the line, and $\alpha^2 = Z_TY_T$. The parameters of relations 2 may be put into our final form by expanding $\cosh\alpha$ and $\sinh\alpha$ in McLaurin's series, giving

$$\left.\begin{aligned} A(\alpha) &= \cosh\alpha \\ &= 1+\frac{\alpha^2}{2!}+\frac{\alpha^4}{4!}+\frac{\alpha^6}{6!}+\ldots\ldots & \text{(a)} \\ B'(\alpha) &= Z_T\frac{\sinh\alpha}{\alpha} \\ &= Z_T\left(1+\frac{\alpha^2}{3!}+\frac{\alpha^4}{5!}+\frac{\alpha^6}{7!}+\ldots\ldots\right) & \text{(b)} \\ C'(\alpha) &= Y_T\frac{\sinh\alpha}{\alpha} \\ &= (Y_T/Z_T)B'(\alpha) & \text{(c)} \\ D(\alpha) &= A(\alpha) & \text{(d)} \end{aligned}\right\} \quad (3)$$

It will be useful to introduce the notation

$$\left.\begin{aligned} B(\alpha) &= B'(\alpha)/Z_T \\ C(\alpha) &= C'(\alpha)/Y_T \end{aligned}\right\} = 1+\frac{\alpha^2}{3!}+\frac{\alpha^4}{5!}+\frac{\alpha^6}{7!}+\ldots\ldots \quad (4)$$

for future use.

Turning to a consideration of the ladder structure let us consider Figure 2 which shows, in the general case, the two possible forms of such a network. It is shown in Appendix I that for either of these circuits the parameters will be of the forms

$$\left.\begin{aligned} A(\alpha) &= a_0+a_1\alpha^2+a_2\alpha^4+\ldots a_{S_1}\alpha^{2S_1} \\ B(\alpha) &= b_0+b_1\alpha^2+b_2\alpha^4+\ldots b_{S_2}\alpha^{2S_2} \\ C(\alpha) &= c_0+c_1\alpha^2+c_2\alpha^4+\ldots c_{S_3}\alpha^{2S_3} \\ D(\alpha) &= d_0+d_1\alpha^2+d_2\alpha^4+\ldots d_S\,\alpha^{2S_4} \end{aligned}\right\} \quad (5)$$

The a, b, c, and d coefficients are functions of the k factors and it is further shown in Appendix I that, when the two circuits of Figure 2 are related as indicated in the figure, then

$$\left.\begin{aligned} A_a &= D_b \\ B_a &= C_b \\ C_a &= B_b \\ D_a &= A_b \end{aligned}\right\} \quad (6)$$

where the subscripts refer to the intended circuit of the figure. In view of this it is necessary to apply the future treatment to only the series-terminated network because the solution for the corresponding shunt-terminated circuit will be tacitly included. We will be dealing with a uniform network and therefore for our purpose $A = D$.

The Uniform Ladder Network

This discussion will apply to a ladder network consisting of M T sections with midseries termination. Such a circuit may be represented by Figure 2a in which $k_1 = k_N = 1/2M$ and all other k's are equal to $1/M$. In similarity with equation 2, when the network is specified by its characteristic impedance (Z_0) and propagation function (γ), we have the well-known relations

$$\left.\begin{aligned} E_s &= (\cosh M\gamma)E_r + Z_0(\sinh M\gamma)I_r \\ I_s &= (1/Z_0)(\sinh M\gamma)E_r + (\cosh M\gamma)I_r \end{aligned}\right\} \quad (7)$$

Thus, for this circuit

$$\left.\begin{aligned} A_T(\alpha) &= D_T(\alpha) = \cosh M\gamma & \text{(a)} \\ B_T(\alpha) &= (Z_0/Z_T)\sinh M\gamma & \text{(b)} \\ C_T(\alpha) &= (1/Z_0Y_T)\sinh M\gamma & \text{(c)} \end{aligned}\right\} \quad (8)$$

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and hydraulic lowering and raising means. It is evident that the output energy of such a regulator depends on the rating and size of the electrode motor. The size of the furnace having balanced electrodes which this type of regulator can handle depends therefore on the energy requirements of the electrode motors. For other types of furnaces certain changes in design would be required such as braking means for the motors. In the application described this has not been found necessary, thus resulting in simplification of the regulating equipment.

The T subscript on A, B, and C is used to indicate that the network is made up of T sections. Furthermore, Z_0 and γ are given by[1]

$$\left.\begin{aligned} Z_0 &= \frac{\alpha\sqrt{4M^2+\alpha^2}}{2Y_TM} && \text{(a)}\\ e^{\gamma} &= \frac{\sqrt{4M^2+\alpha^2}+\alpha}{\sqrt{4M^2+\alpha^2}-\alpha} && \text{(b)} \end{aligned}\right\} \quad (9)$$

In view of relations 9, and with some reduction after substitution in equations 8, we obtain

$$\left.\begin{aligned} A_T(\alpha) &= \frac{(4M^2+\alpha^2)^M}{2(4M^2)^M}\times \\ &\quad \left[\left(1+\frac{\alpha}{\sqrt{4M^2+\alpha^2}}\right)^{2M}+\left(1-\frac{\alpha}{\sqrt{4M^2+\alpha^2}}\right)^{2M}\right] && \text{(a)}\\ B_T(\alpha) &= \frac{(4M^2+\alpha^2)^{M+1/2}}{\alpha(4M)(4M^2)^M}\times \\ &\quad \left[\left(1+\frac{\alpha}{\sqrt{4M^2+\alpha^2}}\right)^{2M}-\left(1-\frac{\alpha}{\sqrt{4M^2+\alpha^2}}\right)^{2M}\right] && \text{(b)}\\ C_T(\alpha) &= \frac{M(4M^2+\alpha^2)^{M-1/2}}{\alpha(4M^2)^M}\times \\ &\quad \left[\left(1+\frac{\alpha}{\sqrt{4M^2+\alpha^2}}\right)^{2M}-\left(1-\frac{\alpha}{\sqrt{4M^2+\alpha^2}}\right)^{2M}\right] && \text{(c)} \end{aligned}\right\} \quad (10)$$

The binomial theorem may be applied twice to finally arrive at a power-series representation of equations 10. The details of the conversion are given in Appendix II, and the results are

$$\left.\begin{aligned} A_T(\alpha) &= \sum_{\lambda=0}^{M} a_\lambda \alpha^{2\lambda} && \text{(a)}\\ B_T(\alpha) &= \sum_{\lambda=0}^{M} b_\lambda \alpha^{2\lambda} && \text{(b)}\\ C_T(\alpha) &= \sum_{\lambda=0}^{M-1} c_\lambda \alpha^{2\lambda} && \text{(c)} \end{aligned}\right\} \quad (11)$$

where

$$\left.\begin{aligned} a_\lambda &= \sum_{n=0}^{\lambda} \frac{(2M)!(M-n)!}{(2M)^{2\lambda}(2M-2n)!(2n)!\times(M-\lambda)!(\lambda-n)!} && \text{(a)}\\ b_\lambda &= \sum_{n=0}^{\lambda} \frac{(2M-1)!(M-n)!}{(2M)^{2\lambda}(2M-2n-1)!\times(2n+1)!(M-\lambda)!\times(\lambda-n)!} && \text{(b)}\\ c_\lambda &= \sum_{n=0}^{\lambda} \frac{(2M-1)!(M-n-1)!}{(2M)^{2\lambda}(2M-2n-1)!\times(2n+1)!(M-\lambda-1)!\times(\lambda-n)!} && \text{(c)} \end{aligned}\right\} \quad (12)$$

The preceding expressions are cumbersome but may be reduced to the following simple forms by the method indicated in Appendix III. The forms are:

$$\left.\begin{aligned} a_\lambda &= \frac{K_a(M,\lambda)}{(2\lambda)!} && \text{(a)}\\ {}_\lambda &= \frac{K_b(M,\lambda)}{(2\lambda+1)!} && \text{(b)}\\ c_\lambda &= \frac{K_c(M,\lambda)}{(2\lambda+1)!} && \text{(c)} \end{aligned}\right\} \quad (13)$$

where

$$\left.\begin{aligned} K_a(M,\lambda) &= \frac{(M+\lambda-1)!}{M^{2\lambda-1}(M-\lambda)!} && \text{(a)}\\ K_b(M,\lambda) &= \frac{(2M^2+\lambda)(M+\lambda-1)!}{2M^{2\lambda+1}(M-\lambda)!} && \text{(b)}\\ K_c(M,\lambda) &= \frac{(M+\lambda)!}{M^{2\lambda+1}(M-\lambda-1)!} && \text{(c)} \end{aligned}\right\} \quad (14)$$

Equations 13 have been written with the factorials in evidence to bring out the similarity with the corresponding coefficients for a smooth line as given by equations 3a and 4. That is, the coefficients for a uniform ladder network are the corresponding coefficients for a smooth line multiplied by correction factors which are given explicitly by equations 14.

Relations Among the Coefficients

The similarities among formulas 14 leads one to look for useful relations among them, and the following recurrence formulas are immediately apparent.

$$\left.\begin{aligned} K_a(M,\lambda) &= \left[1-\left(\frac{\lambda-1}{M}\right)^2\right]\times K_a(M,\lambda-1) && \text{(a)}\\ K_b(M,\lambda) &= \left[1-\left(\frac{\lambda-1}{M}\right)^2\right]\times \frac{\left(1+\dfrac{\lambda}{2M^2}\right)}{\left(1+\dfrac{\lambda-1}{2M^2}\right)}K_b(M,\lambda-1) && \text{(b)}\\ K_c(M,\lambda) &= \left[1-\left(\frac{\lambda}{M}\right)^2\right]\times K_c(M,\lambda-1) && \text{(c)} \end{aligned}\right\} \quad (15)$$

In view of the complexity of equation 15b a possibly more useful form for $K_b(M, \lambda)$ is desirable, and may be obtained by expressing it in terms of $K_a(M, \lambda)$. For completeness a similar form is given for $K_c(M,. \lambda)$. The results are self-evident from equations 14, and are

$$\left.\begin{aligned} K_b(M,\lambda) &= \left(1+\frac{\lambda}{2M^2}\right)K_a(M,\lambda) && \text{(a)}\\ K_c(M,\lambda) &= \left(1-\frac{\lambda^2}{M^2}\right)K_a(M,\lambda) && \text{(b)} \end{aligned}\right\} \quad (16)$$

Other relations can be found among the coefficients; but, because of their particular interest, only the following ones will be given. By writing equation 16b with λ replaced by $\lambda-1$ and substituting the resulting expression for $K_a(M, \lambda-1)$ in equation 15a one obtains the simple relation

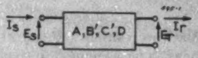

Figure 1

$$K_a(M,\lambda) = K_c(M,\lambda-1) \quad (17)$$

Two less obvious relations may be obtained by starting from fundamental equations of the network. From equations 8 and 9 one may obtain

$$B = (1+\alpha^2/4M^2)C \quad (18)$$

This is an identity and therefore when B and C are replaced by their series representations the corresponding coefficients of a given power of α, on the two sides of the equation, must be the same. Inspection shows that this leads to the result

$$b_\lambda = c_\lambda + \frac{1}{4M^2}c_{\lambda-1} \quad (19)$$

which is valid for all values of λ if c_{-1} is defined as zero. One obtains the second of these two relations by starting from the well-known formula[2]

$$A^2-1 = \alpha^2BC = \left(\alpha^2+\frac{\alpha^4}{4M^2}\right)C^2 \quad (20)$$

Table I

λ	$K_a(M, \lambda)$ or $^*K_c(M, \lambda-1)$	$K_b(M, \lambda)$
0	1	1
1	1	$1+\frac{1}{2M^2}$
2	$1-\frac{1}{M^2}$	$1-\frac{1}{M^4}$
3	$1-\frac{5}{M^2}+\frac{4}{M^4}$	$1-\frac{7}{2M^2}-\frac{7}{2M^4}+\frac{6}{M^6}$
4	$1-\frac{14}{M^2}+\frac{49}{M^4}-\frac{36}{M^6}$	$1-\frac{12}{M^2}+\frac{21}{M^4}+\frac{62}{M^6}-\frac{72}{M^8}$
5	$1-\frac{30}{M^2}+\frac{273}{M^4}-\frac{820}{M^6}+\frac{576}{M^8}$	$1-\frac{55}{2M^2}+\frac{198}{M^4}-\frac{275}{2M^6}-\frac{1,474}{M^8}+\frac{1,440}{M^{10}}$

* This has no meaning for $\lambda=0$.

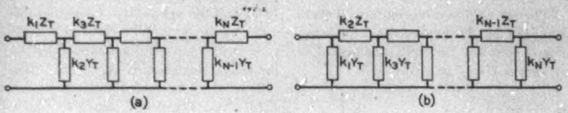

Figure 2

As in the previous case, one may substitute series for A and C and equate coefficients of like powers of α to give the formula

$$\sum_{q=0}^{\lambda} a_q a_{\lambda-q} = \sum_{q=0}^{\lambda-1} c_q c_{\lambda-1-q} + \frac{1}{4M^2}\sum_{q=0}^{\lambda-2} c_q c_{\lambda-2-q} \tag{21}$$

These last two relations (19 and 21) are superfluous so far as numerical computation is concerned but they are included for possible academic interest.

Additional Forms for the Coefficients

Neither of the forms so far given, in equations 12 or 13, for the coefficients gives a clear indication of how they vary with M, at a fixed value of λ. A better form, in this respect, may be obtained for $K_a(M, \lambda)$ and $K_c(M, \lambda)$ by the successive use of formulas 15a and 15c, and for $K_b(M, \lambda)$ by applying equation 16a to the new formula for $K_a(M, \lambda)$. The algebra of this transformation is self-evident, and the results are

$$\left.\begin{aligned} K_a(M,\lambda) &= \prod_{n=0}^{\lambda-1}\left(1-\frac{n^2}{M^2}\right) && \text{(a)}\\ K_b(M,\lambda) &= \left(1+\frac{\lambda}{2M^2}\right)\times \prod_{n=0}^{\lambda-1}\left(1-\frac{n^2}{M^2}\right) && \text{(b)}\\ K_c(M,\lambda) &= \prod_{n=0}^{\lambda}\left(1-\frac{n^2}{M^2}\right) && \text{(c)}\end{aligned}\right\} \tag{22}$$

From these it is seen that $K_a(M, \lambda)$ is a polynomial in even powers of $1/M$, the powers ranging from zero to $2\lambda-2$, inclusive; and that $K_b(M, \lambda)$ and $K_c(M, \lambda)$ are similar, but with even powers ranging from zero to 2λ, inclusive. Some of these polynomials have been computed and are given in Table I. It is interesting to note that equations 22 put in evidence the fact that a_λ and b_λ are zero if $\lambda > M$; and that c_λ is zero when $\lambda > M-1$. The form of equations 22 is particularly useful because it allows one to estimate the coefficients in terms of upper and lower bounds which may be obtained from the relation

$$\left[1-\left(\frac{\beta+1}{2M}\right)^2\right]^\eta\left(1-\frac{\beta^2+1}{M^2}\right)^{[\beta/2]} < \prod_{n=0}^{\beta}\left(1-\frac{n^2}{M^2}\right) < \left[1-\left(\frac{\beta+1}{2M}\right)^2\right]^\beta \underset{\beta<M}{<1} \tag{23}$$

in which $\eta=0$ or 1, depending on whether β is even or odd, and $[\beta/2]$ is the greatest integer in $\beta/2$. The derivation is given in Appendix IV. Taking the right side of this inequality allows one to write

$$\left.\begin{aligned} a_\lambda &< \frac{1}{(2\lambda)!} && \text{(a)}\\ b_\lambda &< \left(1+\frac{1}{2M}\right)\frac{1}{(2\lambda+1)!} && \text{(b)}\\ c_\lambda &< \frac{1}{(2\lambda+1)!} && \text{(c)}\end{aligned}\right\} \tag{24}$$

which is interesting because the reciprocal factorials are the corresponding smooth-line coefficients.

The a, b, and c coefficients can be given in still another form, in terms of the hypergeometric function $F(r, s, t, x)$. When $x=1$ this function reduces to[4]

$$F(r,s,t,1) = \frac{\Gamma(t)\Gamma(t-r-s)}{\Gamma(t-s)\Gamma(t-r)}, \quad \text{if real part } (t-r-s)>0. \tag{25}$$

which may be used to represent any one of the expressions in equations 14 by suitable choices for r, s, and t. The following results are given for combinations of r, s, and t which reduce the multiplying factors to simple forms; but other choices would be possible. The correctness of the choices will become evident upon making the indicated substitutions. The results are

$$\left.\begin{aligned} a_\lambda &= \frac{1}{M^{2\lambda-1}}F(1-M+\lambda, 1-2\lambda, 2, 1) && \text{(a)}\\ b_\lambda &= \frac{2M^2+\lambda}{4M^{2\lambda+1}}F(2-M+\lambda, 1-2\lambda, 3, 1) && \text{(b)}\\ c_\lambda &= \frac{1}{M^{2\lambda+1}}F(1-M+\lambda, -1-2\lambda, 1, 1) && \text{(c)}\end{aligned}\right\} \tag{26}$$

Conclusions

In view of the mathematical form of the results the formulas themselves are their most concise representation and only a few interpretive remarks are considered necessary. In the use of these results it is to be remembered that, as given, they apply to the midseries termination; but that the midshunt-terminated case is represented if B and C are interchanged. Of the several forms given probably equations 14 and 22 are the most important. If all the coefficients are to be found, equations 22, or the recurrence formulas which are practically the equivalent, will be found to be the more convenient. However, if only an isolated coefficient is wanted it may be computed more conveniently by employing equations 14 in conjunction with tables of logarithms of the factorials, or Stirling's approximation, which is

$$\log_{10}n! = (n+{}^1/_2)\log_{10}n - n\log_{10}e + ({}^1/_2)\log_{10}2\pi \tag{27}$$

Equations 22 have the greater utility from the interpretive standpoint. It has already been pointed out that this form puts in evidence the number of nonzero coefficients in the series for each parameter. The form also leads to relation 23 which will yield a quick estimate of a coefficient and may therefore be useful when M and λ are large (but λ not too near M) and the required accuracy does not necessitate the more extended computation of the exact formula. This inequality also leads to the interesting results given in relations 24; namely, that the coefficients or the A and C parameters are always less than the corresponding smooth-line coefficients, and that, for the B parameter, the ladder-network coefficients are less than $(1+1/2M)$ times the corresponding smooth-line coefficient.

The treatment has been entirely in terms of the A-B-C parameters. However the four-terminal network may also be specified in terms of the driving-point and transfer impedances (or admittances). The results are readily extended to give the latter parameters as rational functions of α^2 because the two types of parameters are related by simple rational forms. This ability to easily write the parameters as rational functions would find practical application where one is required to find the zeros or poles of these functions (or rational combinations of them), with respect to the variable $p=d/dt$, because explicit means are available for finding the zeros of a polynomial. It is well known that such zeros and poles are of importance in circuit analysis. In relation to this discussion it should be noted that α^2 is a rational function of p when the network consists of lumped elements.

Appendix I

For the network of Figure 2a we may write the $(N+1)/2$ mesh equations

$$\left.\begin{aligned} E_sY_T &= I_s(k_1\alpha^2+1/k_2) - I_3/k_2\\ 0 &= -I_s/k_2 + I_3(1/k_2 + k_3\alpha^2 + 1/k_4) - I_5/k_4\\ &\cdots\cdots\cdots\cdots\\ 0 &= -I_{N-1}/k_{N-1} + I_r(1/k_{N-1} + k_N\alpha^2) + E_rY_T\end{aligned}\right\} \tag{28}$$

where the numbered currents are the mesh currents which are numbered to correspond to the series impedances through which they flow. Figure 2b may be given a similar treatment, but on a nodal basis,[8] with the result which follows:

$$\left.\begin{aligned} I_sZ_T &= E_s(k_1\alpha^2+1/k_2)-E_3/k_2 \\ 0 &= -E_s/k_2+E_3(1/k_2+ \\ &\qquad k_3\alpha^2+1/k_4)-E_5/k_4 \\ &\cdots\cdots\cdots\cdots \\ 0 &= -E_{N-1}/k_{N-1}+E_r(1/k_{N-1}+ \\ &\qquad k_N\alpha^2)+I_rZ_T \end{aligned}\right\} \quad (29)$$

In these the voltage subscripts refer to the nodes at which the shunt elements having the same subscripts are terminated. It is seen that these two sets of equations are identical with respect to the k's and α, and are dually related with respect to voltage, current, impedance, and admittance. Equations 28 may be solved for E_s and I_s, in terms of E_r and I_r, and the solutions are

$$\left.\begin{aligned} E_s &= A(\alpha,k_1,k_2,\ldots)E_r+ \\ &\qquad Z_TB(\alpha,k_1,k_2,\ldots)I_r \\ I_s &= Y_TC(\alpha,k_1,k_2,\ldots)E_r+ \\ &\qquad D(\alpha,k_1,k_2,\ldots)I_r \end{aligned}\right\} \quad (30)$$

where A, B, C, and D are polynomials in α, the coefficients of which are functions of the k's. The above-mentioned relation between equations 28 and 29 shows that the solutions of equations 29 are

$$\left.\begin{aligned} I_s &= A(\alpha,k_1,k_2,\ldots)I_r+ \\ &\qquad Y_TB(\alpha,k_1,k_2,\ldots)E_r \\ E_s &= Z_TC(\alpha,k_1,k_2,\ldots)I_r+ \\ &\qquad D(\alpha,k_1,k_2,\ldots)E_r \end{aligned}\right\} \quad (31)$$

Relations 6 follow from a comparison of equations 30 and 31.

Appendix II

The method used to arrive at the formulas for A, B, and C for the uniform ladder network will be given by the use of A as an example. The same procedure was used for B and C. We start by expanding each of the binomials of equation 10a by the binomial theorem. The signs are such that odd powers of the argument cancel and we obtain

$$A=\frac{1}{(4M^2)^M}\sum_{r=0}^{M}\frac{(2M)!}{(2M-2r)!(2r)!}\times (\alpha^2)^r(4M^2+\alpha^2)^{M-r} \quad (32)$$

The binomial expansion may now be applied to the last term in the summation, giving

$$A=\sum_{r=0}^{M}\frac{(2M)!}{(4M^2)^r(2M-2r)!(2r)!}(\alpha^2)^r\times \sum_{s=0}^{M-r}\frac{(M-r)!}{(M-r-s)!(s)!}\left(\frac{\alpha^2}{4M^2}\right)^s \quad (33)$$

which may be written as

$$A=\sum_{r=0}^{M}\frac{(2M)!}{(2M-2r)!(2r)!}\times \sum_{s=0}^{M-r}\frac{(M-r)!}{(M-r-s)!(s)!}\left(\frac{\alpha^2}{4M^2}\right)^{r+s} \quad (34)$$

This is conveniently transformed by changing the summation indices to r and u, where $u=r+s$, resulting in

$$A=\sum_{r=0}^{M}\frac{(2M)!(M-r)!}{(2M-2r)!(2r)!}\times \sum_{u=r}^{M}\frac{1}{(M-u)!(u-r)!}\left(\frac{\alpha}{2M}\right)^{2u} \quad (35)$$

The above may be rearranged by factoring out the various powers of α. For example, let λ be a particular value of u, it being obvious that λ may be between 0 and M, inclusive. For this value, one coefficient of $\alpha^{2\lambda}$ will be obtained for each value of r in the first summation for which $\lambda \geqq r$, because the lower limit on the second summation is r. Thus, r may be replaced by another index (n) which runs from 0 to λ, inclusive, and the result is as given by the combination of equations 11a and 12a.

Appendix III

An outline of the complete proof for the reduction to forms 14 will be given for a_λ. The usual notation for a binomial coefficient will be used, namely

$$\binom{p}{q}=\frac{p!}{q!(p-q)!}$$

In this notation we are to prove

$$\sum_{n=0}^{\lambda}\binom{2M}{2n}\binom{M-n}{\lambda-n}=\frac{2^{2\lambda}M(M+\lambda-1)!}{(2\lambda)!(M-\lambda)!} \quad (36)$$

The method is to write the function

$$\phi(x)=(1-x^2)^{-M+\lambda-1}(1+x)^{2M}=\sum_{q=0}^{\infty}G_qx^q \quad (37)$$

and obtain two expressions for $G_{2\lambda}$, one which will be the left side of equation 36 and the other which will be the right side. The function is analytic and the series converges when $|x|<1$. With some manipulation for the first of the following forms, and directly for the second, one obtains

$$\left.\begin{aligned} (1-x^2)^{-M+\lambda-1} &= \sum_{r=0}^{\infty}\binom{M-\lambda+r}{r}x^{2r} \quad &(a) \\ (1+x)^{2M} &= \sum_{s=0}^{2M}\binom{2M}{s}x^s \quad &(b) \end{aligned}\right\} \quad (38)$$

$G_{2\lambda}$ is obtained from equations 38 by summing the product of the general terms of these two series over all values of r and s such that $r\geqq 0$, $0\leqq s\leqq 2M$, and $2r+s=2\lambda$. Thus, s must be even, say $2n$, and therefore $r=\lambda-n$. With the substitution of these in the general terms of equations 38, and taking their products, one arrives at the left side of equation 36.

The second part of the proof is obtained from the Cauchy integral formula for $\phi^{(2\lambda)}(0)/(2\lambda)!$ (the McLaurin-series coefficient of $x^{2\lambda}$). This formula is

$$G_{2\lambda}=\frac{1}{2\pi j}\int_C\frac{\phi(x)}{x^{2\lambda+1}}dx \quad (39)$$

where C is a circle of radius less than unity, with its center at the origin, and over which the integration is to be in a counterclockwise direction. The integral is easily evaluated by employing the change of variable $x=1/u$ which results in

$$G_{2\lambda}=\frac{1}{2\pi j}\int_{C'}u(u+1)^{M+\lambda-1}(u-1)^{-M+\lambda-1}du \quad (40)$$

taken counterclockwise about the origin on a circle of radius greater than unity. The integrand has a pole at $u=1$, which is inside the path of integration, and is analytic everywhere else inside the path of integration. Hence, $G_{2\lambda}$ is the residue of the integrand at $u=1$. The Laurent series for $\psi(u)$, the integrand, may be shown to be

$$\psi(u)=2^{M+\lambda-1}\sum_{r=0}^{M+\lambda-1}\frac{1}{2^r}\binom{M+\lambda-1}{r}\times (u-1)^{-M+\lambda-1+r}+2^{M+\lambda-1}\times \sum_{r=0}^{M+\lambda-1}\frac{1}{2^r}\binom{M+\lambda-1}{r}(u-1)^{-M+\lambda+r} \quad (41)$$

The residue is the coefficient of $(u-1)^{-1}$ and is obtained by putting $r=M-\lambda$ in the first sum, and $r=M-\lambda-1$ in the second. The resulting form is readily reducible to the right side of equation 36.

Similar proofs may be given for equations 14b and 14c. In both cases one finds two formulas for the coefficient of $x^{2\lambda+1}$ in the expansion of $\phi(x)$; but in the case of equation 14c the function $(1-x^2)^{-M+\lambda}$ is substituted for $(1-x^2)^{-M+\lambda-1}$ in $\phi(x)$.

Appendix IV

To arrive at relation 23 observe that the continued product may be written

$$\prod_{n=0}^{\beta}\left(1-\frac{n^2}{M^2}\right)=\left[1-\left(\frac{\beta+1}{2M}\right)^2\right]^\eta\times \prod_{n=0}^{\left[\frac{\beta}{2}-1\right]}\left[1-\left(\frac{\frac{\beta+1}{2}-\Delta}{M}\right)^2\right]\times \left[1-\left(\frac{\frac{\beta+1}{2}+\Delta}{M}\right)^2\right] \quad (42)$$

where

if β is even, $\eta=0$ and $\Delta=n+1/2$
if β is odd, $\eta=1$ and $\Delta=n+1$

and the brackets on $\beta/2-1$ indicate the greatest integer in $\beta/2-1$. The continued

Improved Selective Tripping of Low-Voltage Air Circuit Breakers

CHARLES P. WEST
MEMBER AIEE

SINCE the first devices for interrupting short-circuit currents in electric systems were invented, the problem of having them operate to clear faults without removing energy from normal circuits has existed. Many devices, both separate and as parts of the interrupters, have been developed and good results obtained. Systems having potentials below 600 volts alternating current normally use air interrupting devices designed particularly for this service.

The normal utilization of a-c power in industrial plants or generating station auxiliary circuits where the average individual load is less than about 75 horsepower is at 600 volts or less. Occasional larger loads and groups of smaller loads, switched as units, require a considerable range in both current-carrying capacity and interrupting ability of circuit breakers. The standard devices available have current ratings up to 6,000 a-c amperes and 100,000 amperes interrupting rating. Larger units have also been built to a limited extent.

When a large number of small loads are supplied, the total power required determines the size of the generating or transforming apparatus and thus indirectly the values of fault currents. Even though individual loads may be small, high interrupting requirements may dictate the use of relatively large circuit breakers. They may become so large that their cost is out of proportion to the motors or other devices to which they supply power. The economics of low-voltage system design, therefore, require circuit arrangements which will permit the use of the smallest possible switching devices. At the same time, selective isolation of faults is highly desirable. However, this is frequently partially sacrificed in special circuits arranged to permit the use of circuit breakers in duty above their interrupting ratings (cascading). New developments in built-in tripping devices will provide full selectivity without the use of separate relays in normal breaker applications and increase the ranges of reliable selective tripping in special circuit arrangements in which breakers are applied whose interrupting capacities are below the value of maximum fault currents.

Selective Tripping With Breakers Having Full Interrupting Ability

Circuit breakers supplying power to loads should operate to clear only faulted circuits and not interrupt energy to other parts of the system. This requirement can sometimes be modified, but in circuits for essential central-station auxiliaries and certain industrial processes, maximum continuity of service is essential. This means, referring to Figure 1, that breakers *A*, *B*, and *C* must have interrupting capacity fully equal to the fault currents which may occur, that they must have time discrimination on tripping, and that they must have short-time ratings adequate for the time and current to which they will be subjected. These distinctions, as compared with the special "cascade" arrangement discussed later, where only limited selectivity is obtainable, should be kept in mind throughout the following discussion.

The problems arising from selective tripping are all recognized in high-voltage breaker applications and standard devices meet them. These are usually protective relays which open the breakers by energizing their shunt trip coils. The situation on low-voltage air breakers is somewhat different. The designs of all manufacturers have almost uniformly included instantaneous trip attachments operating at 15 times normal current on ratings below 2,000 amperes and 12 times normal above this value. No short-time current ratings were assigned. This has meant that the amount of conducting material in the contacts is relatively less than has been considered necessary in high-voltage designs. Moves are now under way to assign short-time ratings to all low-voltage breakers. However, the maximum values possible for existing breakers, for reasons just explained, probably will be somewhat lower compared to the interrupting current rating, than has been the case with high-voltage power breakers. This must be taken into consideration in applying breakers where intentional time delay in tripping is introduced to obtain selectivity and affects cost as compared to the cascade arrangement described in detail later.

When breakers *B* and *C* are in separate structures and connected by cables, the problem of protecting these cables enters the picture. A relatively long tripping time is desirable, operating at the cable current capacity, a shorter tripping time within the short-time current rating of the breaker is needed for selectivity and instantaneous tripping above this current. Present commercially available self-contained tripping devices offer only the time characteristics of oil dashpots or thermal devices and these, at best, are questionable. Protective relays offer the best available means for obtaining the tripping time values necessary for these various current values.

Many engineers are satisfied to have short-circuit protection only on power supply cables on the basis that they are protected from overloads by the feeder breakers. This is sound practice on a properly designed distribution system,

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product on the right side of equation 42 may be written

$$\prod_{n=0}^{\left[\frac{\beta}{2}-1\right]}\left[1-\frac{\beta^2+2\beta+1+4\Delta^2}{2M^2}+\frac{(\beta^2+2\beta+1-4\Delta^2)^2}{16M^4}\right] \quad (43)$$

A lower bound of this quantity may be obtained by neglecting the third term and assigning to Δ its largest value, namely $(\beta-1)/2$ in each case. Similarly, an upper bound will result if $4\Delta^2$ is neglected in all terms. These substitutions, by eliminating the variable n, permit the substitution of an exponent for the product, and readily yield the results given in relation 23.

References

1. COMMUNICATION NETWORKS (book), E. A. Guillemin. John Wiley and Sons. Volume II, page 178.

2. Reference 1, page 139.

3. ELECTRIC CIRCUITS (book), Massachusetts Institute of Technology staff. John Wiley and Sons. Page 463.

4. MODERN ANALYSIS (book), Whittaker and Watson. Cambridge University Press. Fourth edition, page 281.

but there are arrangements where the best planning cannot eliminate the hazard of overloading. In these cases, discriminatory tripping must be provided or continuity of service partially sacrificed.

The application of relays for obtaining selective tripping with suitable breakers on the basis of time discrimination is well understood and only self-contained tripping devices will be considered here.

Cascaded Breakers

If we refer again to Figure 1 which illustrates typical circuits, it is evident that if breaker *B* and one of the *C* breakers both open at substantially the same time to clear a fault, the interrupting duty on *C* will be materially reduced. If tripping devices are supplied for *B* so that this will always take place, then a smaller breaker can under certain conditions be used in position *C* than would have been possible in the circuits described previously. A similar relation exists between breakers *A* and *B*. This often results in a material saving in the cost of circuit breakers. The advantages, limitations and conditions determining the selection of interrupting devices in cascade circuits of this kind will be considered.

Manufacturers have recognized the advantages of this arrangement and have approved special cascaded circuits as covered by the following definition in the National Electrical Manufacturers Association Standards.

> "Cascading is the application of air circuit breakers in which only the breakers nearest the source of power shall have interrupting ratings equal to, or in excess of, the obtainable fault current while breakers in succeeding steps further from the source may have successively lower interrupting ratings."

The relative allowable reduction in interrupting rating of the breakers in successive steps is limited as follows. Group breakers *B* may have 50 per cent of the interrupting requirements of main breaker *A* and branch breakers *C* in turn may have one-third the rating of the main breaker which must be able to open on a standard duty cycle the maximum fault current which the power supply can deliver. Cascading is limited to three steps of interrupting rating. In many applications, this represents a great saving in breaker cost as compared to an installation in which all devices have full interrupting ability. However, it must be realized that this saving is not obtained without some risk and sacrifice in over-all performance.

The main and group breakers must be free to trip instantaneously to afford maximum protection to the smaller branch breakers which are allowed to be of reduced interrupting ratings. This requirement is the all-important and definite difference between cascaded and similar circuits previously described in which all breakers have the same full interrupting ability and selectivity of tripping depends on time discrimination.

The instantaneous tripping devices of the main and group breakers must be set at about 80 per cent of the interrupting rating of those in the next lower step in the cascade. This margin is necessary to allow for some manufacturing variances and to insure that the speed of opening will not be too slow at currents just over the interrupting ability of the breaker in the next lower step. These trip calibrations mean that faults on load circuits may open the group or even the main breaker depending on whether the current reaches the trip settings of these devices. This limitation is fundamental in the cascade arrangement and must be accepted as one of the offsetting factors against the savings in equipment.

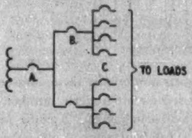


Figure 1. A common arrangement of low-voltage breakers

At present, low-voltage air breakers have no published short-time current ratings. These are being determined by test for existing designs and will soon be announced. When this takes place, the tripping devices on main and group breakers may be set to allow delayed tripping up to their short-time rating with instantaneous opening at currents above this figure. It is apparent that better continuity of service and selective isolation of faults will be obtained under these conditions. However, there will still be many conditions in cascaded circuits having breakers with successively reduced interrupting and short-time ratings where opening must be instantaneous.

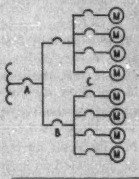


Figure 2. A typical cascaded power supply to motors listing the breaker characteristics for co-ordinated mechanically timed tripping

	Breaker *A*	Breaker *B*	Breaker C
Current rating, amperes	1,600	600	50
Interrupting rating, amperes	50,000	25,000	15,000
Half-second short-time rating, amperes	25,000	18,000	10,000
Instantaneous trip setting, amperes	20,000	14,400	500
Time-delayed trip settings, seconds	0.4	0.2	*

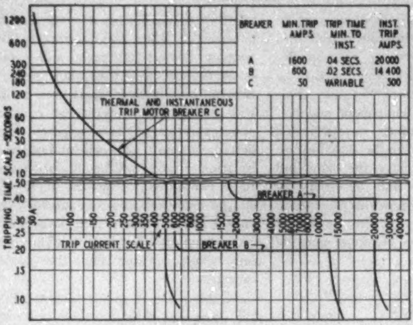
* Thermal time delay set to allow motor starting and provide motor overload protection.

At this point, it should be noted that the short-time ratings which can be assigned to low-voltage breakers designed for instantaneous opening on 12 or 15 times their continuous rating will have to be materially lower than their interrupting rating. This does not parallel the situation on higher-voltage circuit breakers where short-time ratings equal or occasionally exceed their interrupting ratings. It is also very doubtful whether the cost of future low-voltage designs should be increased to provide higher short-time ratings. Most applications can be satisfied with present constructions and an increased number of standard ratings is not economically desirable. The requirements of a few applications should not burden all the others by dictating high short-time ratings in all standard designs.

A reference to Figure 1 will make it evident that:

1. Breaker *B* must open instantaneously when its short-time rating is exceeded.
2. In the cascade arrangement, with successively smaller steps of interrupting capacity and short-time ratings, close-in faults on feeders may exceed the short-time ratings of the group breaker.
3. Consequently selective tripping is not always possible.

A further condition applying to cascaded breakers is the restricted duty cycle which is imposed. A breaker operated within its rating is allowed maximum-current "O" and "CO" operations separated by a two-minute interval and should be able to carry rated load after such openings. Breakers operating above their rating in a cascade circuit are restricted by requiring inspection and possible maintenance after each opening on fault current. This means that it is not safe to reclose cascaded breakers, even once, without inspection if two in series have cleared a short circuit. The smaller breaker might be damaged so that, even though its contacts completed the circuit, they would not safely carry



BREAKER	MIN. TRIP AMPS.	TRIP TIME MIN. TO INST.	INST. TRIP AMPS.
A	1600	.04 SECS.	20000
B	600	.02 SECS.	14 400
C	50	VARIABLE	500

Figure 3. Time-current tripping co-ordination of cascaded breakers with integral mechanically delayed trips

The horizontal lines represent time bands of appreciable width and must be separated approximately as shown

load current. Inspection is necessary after each fault opening when breakers are applied with inadequate interrupting ability as in cascaded circuits.

To sum up the case for cascaded breaker circuits; on the affirmative side, breaker expense is often materially reduced; on the negative side, continuity of power is partially sacrificed, increased breaker maintenance may be expected, and a more limited duty cycle is specified for most of the breakers. Cascade arrangements applied with a full understanding of these conditions should prove satisfactory.

Cascaded Breakers With Selective Tripping

New definite-time tripping devices are being developed which will give breakers so equipped equal or better selectivity than is obtainable with relays, and will also improve the operation of cascade arrangements. Tests with as many as three breakers in series have shown accurate selective tripping with a maximum time setting of less than one-half second on the largest breakers. The diagram, Figure 2, has been prepared to illustrate such conditions. This arrangement of breakers will provide instantaneous tripping in the ranges required to permit a cascaded circuit with the allowed reduction in interrupting rating in each breaker step farther from the supply. The consequent economics are realized and the new tripping devices will function to give all the selectivity which the chosen breakers will permit.

Let us now review the operation of this cascaded circuit under a number of fault and load conditions. Assume motor *M* has a full-load current of 50 amperes. Breaker *C* will provide the usual thermal protection. It will open instantaneously on a motor fault exceeding ten times normal or 500 amperes. If the fault is over 15,000 amperes, that is, above the interrupting ability of *C*, breaker *B* will open instantaneously to back up *C* and so on up through the current range and breaker series. If the fault is below 15,000 amperes but for some reason breaker *C* fails to clear, breaker *B* will still open in 0.2 second. If it, in turn, should not interrupt, breaker *A* will open in 0.4 second. Faults which occur anywhere in the circuit will:

1. Open the nearest breaker.
2. Open the next larger breaker instantaneously if its assistance is needed for interruption.
3. Open the latter breaker in a slightly longer time if the current is within the interrupting ability of the first breaker, but it has failed to clear.
4. Open succeeding breakers in a similar manner if needed.

Reference to Figure 3 will show the current and time characteristics which give these results.

Considering the time intervals during which breakers will be subjected to high fault current in this arrangement, breaker *C* would be a thermally tripped breaker with a self-protecting instantaneous trip set at 500 amperes, breaker *B* would be set to open in 0.2 second on currents up to 14,400 amperes, 80 per cent of its short-time rating, and breaker *A* would be set to trip in 0.4 second, up to 20,000 amperes, 80 per cent of its half-second rating. This allows each breaker, provided its short-time rating is not exceeded, to hold in a sufficient time for the breaker next lower in the cascade to clear. This ensures that all breakers adequate to carry fault current arising from a short circuit remain closed and all others trip.

It is evident that the above degree of selective tripping gives discriminatory opening up to the capacity of the breakers in the cascaded circuit. No better selectivity is possible with breakers applied in accordance with standard cascading practice. However, there may be fault current values where group breakers occasionally open and de-energize unfaulted circuits. The savings due to the use of smaller breakers will usually justify this. However, a similar circuit made up entirely of breakers with definite-time tripping attachments, full interrupting ability, and adequate short-time ratings will give complete selectivity without the use of relays and shunt trip coils.

It has been shown, first, that full selectivity could be obtained with breakers having full interrupting ability and adequate short-time ratings in all steps and tripped by relays giving definite time discrimination in faults. Second, the possibilities and economies of cascaded breakers and the limitations of this system were considered. Third, it was explained that new tripping devices are being developed for standard breakers combining instantaneous and time-delay tripping features so that the economics of cascaded breakers can be realized and short-time ratings and interrupting ability used to the best advantage to give maximum permissible selectivity. It was also pointed out that adequate breakers with definite-time tripping devices in all positions can give complete selectivity, the equivalent of which has previously been possible only with relays, shunt trips, and storage batteries. Thus we advance another step in the economic use of materials coupled with a better accomplishment of purpose in improved continuity of service.

The Degree of Short-Circuit Protection Afforded Small Low-Voltage A-C and D-C Starters by Means of Fuses and Circuit Breakers

B. W. JONES
MEMBER AIEE

AIEE technical paper 42-79, entitled "Thermal Co-ordination of Motors, Control, and Their Branch Circuits on Power Supplies of 600 Volts and Less,"[1] was presented in April 1942. The purpose of the article as stated was "to show what the characteristics of the protective devices should be in order that the power will be disconnected from the main feeders under all conditions of excess current before excess temperatures are attained." After the overload protecting requirements were discussed, it was pointed out that "to protect the conductors in the branch circuit adequately, the control devices, and the motor winding, we must use a device that is both fast in opening the circuit and also has the necessary interrupting ability." In discussing the speed and the interrupting abilities of circuit breakers and fuses which were used to protect the motors and the controls against short circuits, the following statement was made:

> "The second half of the question dealing with the amount of available energy which these fuses can interrupt was determined by providing a 250-volt d-c source which was capable of delivering 96,000 amperes and also a three-phase source which was capable of delivering an rms symmetrical current of 84,000 amperes at 440 volts and 114,000 amperes at 600 volts. In the d-c circuit were placed two standard nonrenewable General Electric 250-volt fuses in series to simulate the usual two fuses that are in a circuit. Two 600-volt fuses were also tested in this circuit wherein the available currents were varied from 10,000 to 96,000 amperes in several steps, and the results were perfect. The buildup of the current in this circuit was much slower than in an a-c circuit so that the current-limiting effects were much more pronounced as shown in Figure 7.
>
> "In the three-phase 440-volt a-c circuit one 250-volt fuse of the preceding type was connected in each supply line, and the available currents ranged from 18,000 to 84,000 symmetrical rms amperes. The resulting currents are shown in Figure 6, and the performance was perfect. When the supply power was 600 volts, then one 600-volt fuse was connected in each supply line, and the available current ranged from 24,000 to 114,000 amperes. The results are also shown on Figure 6 which was perfect in operation. It can be said therefore that *with this arrangement of connections* (note that Figure 6 gives the impedance of each combination of the a-c circuits), these fuses are capable of interrupting a circuit, either a-c or d-c, which has an available short-circuit current of 100,000 amperes."

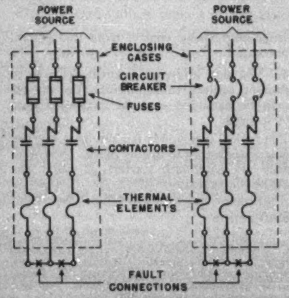


Figure 1. Arrangement of connections for fuses or circuit breakers when used for protecting a-c starters

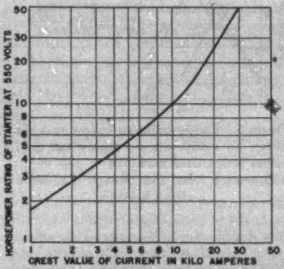


Figure 2. Approximate short-circuit currents (crest value) which will flow through a three-phase across-the-line starter when connected to a 550-volt three-phase supply that has an available current of 50,000 rms symmetrical amperes—short circuit on load side of starter

The preceding statement regarding the interrupting abilities of fuses has been interpreted by some differently from the original intent, and it is the purpose of this supplementary paper to point out the interpretation which should be made. Some further data will be submitted to show the relative protecting characteristics of these fuses and of some small-size air circuit breakers which are commonly used for protecting small-size starters.

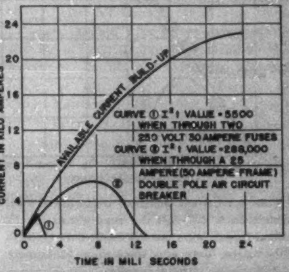


Figure 3. Current build-up following a short circuit on a 250-volt d-c supply and the I^2t values which were permitted to pass through 30-ampere fuses and through a 25-ampere circuit breaker

As was stated in the purpose of the article, the investigation was to ascertain the degree to which the fuses and the circuit breakers would protect the starters from damage due to short circuits passing through the starters. Therefore, every test had a starter in series with either a set of appropriate-size fuses or a corresponding-size circuit breaker as illustrated in Figure 1 of this present paper. Therefore the amount of current which would flow was affected appreciably by the size of starter that was in the circuit. The original statement was not intended to imply that the fuses would give the same performance when the starters were omitted from the circuit. As previously noted, it stated that *with this arrangement of connections* the fuses were capable of interrupting a circuit having an available current of 100,000 amperes.

To emphasize further the importance of this difference, Figure 2 shows the approximate current which would flow if various sizes of three-phase starters, rated according to horsepower at 550-volt three-phase power supply, which had an available short-circuit current of 50,000 rms symmetrical amperes, were connected to the supply when a three-phase short circuit was placed on the load side of the starters. This shows that the value of the short-circuit current is determined more by the size of the starter, for the sizes under con-

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sideration, than by the size of the power supply. Therefore, the small-size fuses have to interrupt the smaller currents and the larger fuses the larger currents which is in accordance with their abilities. Therefore, our tests showed that when the several features that were enumerated in the original paper obtained, then the fuses were able to give the necessary protection as originally claimed.

These data also emphasize the importance of combining the short-circuit protecting means and the starter into one enclosure as illustrated in Figure 1 so as to minimize the probability of a short circuit occurring between the protecting means and the starter. Then, if the short circuit should require the assistance of the protecting means, the starter will be in the path of the short circuit, and the resultant current will be within the range of what the fuse can handle.

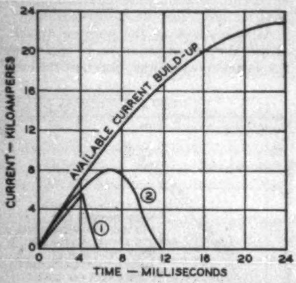

Figure 4. Current build-up following a short circuit on a 250-volt d-c supply and the I^2t values which were permitted to pass through 60-ampere fuses and through a 50-ampere circuit breaker

Curve 1 I^2t value = 62,000 when through two 250-volt 60-ampere fuses

Curve 2 I^2t value = 352,000 when through a 50-ampere double-pole circuit breaker

Also, in support of the superior protection claimed for the fuses in contrast to the protection which circuit breakers give to those small-size starters (size 3 and less), Figures 3, 4, and 5 are offered. The comparison shown is the I^2t values (the current squared times the duration in seconds) which two 250-volt fuses of the 30-, 60-, and 100-ampere sizes and double-pole circuit breakers (standard 50- and 100-ampere frame sizes of breakers) permitted to flow into a short circuit from a 250-volt d-c supply which had an available short-circuit current of 23,000 amperes. The I^2t values which these two devices permit to get through into a short circuit is a direct measure of the amount of heat that will be put into some thermally weak member that should be protected. The reason why a d-c supply was chosen on which to base this comparison was that in many cases it is essential that this short circuit be removed in less than one-half cycle, and if direct current is used then the initial rate of current build-up and the voltage at interruption always will be the same, and whatever difference there is in the I^2t value will be due to the difference in the interrupting characteristics of the two devices. In these three figures the available current from the power source is plotted and, curve 1 the build-up and cutoff as determined by the fuse, and curve 2 the buildup and cutoff as determined by the circuit breaker. It will be noted that two 30-ampere 250-volt fuses are compared to a double-pole 25-ampere (50-ampere frame) circuit breaker; two 60-ampere fuses are compared to a double-pole 50-ampere circuit breaker; and two 100-ampere fuses are compared to a double-pole 100-ampere (100-ampere frame) circuit breaker. These data show that the respective I^2t values which these devices permitted to pass were 5,500 versus 288,000; 62,000 versus 352,000; 170,000 versus 582,000. This very definitely proves that the fuse does a very much superior job in protecting the smaller device against excess energy.

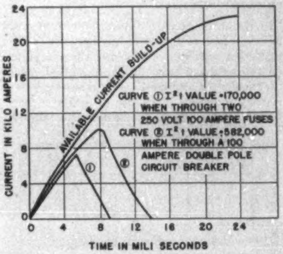

Figure 5. Current build-up following a short circuit on a 250-volt d-c supply and the I^2t values which were permitted to pass through 100-ampere fuses and through 100-ampere circuit breakers

Because of the aforementioned situation, it may be constructive to show the range of thermal capacity of the heaters of the overload relays which are very commonly the thermal limiting factor on most starters. The minimum curve in Figure 6 shows the I^2t values which the overload relay heaters may be able to withstand. As already shown, the 60-ampere fuse would be used in conjunction with an overload relay rated about 17 or 18 amperes, and Figure 6 shows that the minimum value may be about 70,000, and the test data showed that the fuse permitted 62,000 to get through but the circuit breaker permitted 352,000 to pass. The maximum curve shows that there are some forms of relays which have a permissible I^2t value of 700,000. But, since the trend is toward the faster tripping relay so as to protect the fast-heating motors, it is advisable that the fast short-circuit protective means be used so as better to protect either the fast- or the slow-heating type of overload relays.

Conclusions

When we consider that:

1. The fuse is capable of interrupting a short circuit on a large power supply of either alternating or direct current if the starter is in the path of the short circuit.

2. Under some conditions, the 30-ampere fuse will allow only *two per cent* and the 60- and 100-ampere fuses will allow only 25 to 30 per cent of what similar-size circuit breakers will permit to reach a short circuit under similar circumstances.

3. The general trend of motors is to require faster overload relays, and these faster relays generally will have less thermal capacity (more expensive and larger relays can be fast and still have large thermal capacity), but the user cannot be sure when purchasing control from the general market.

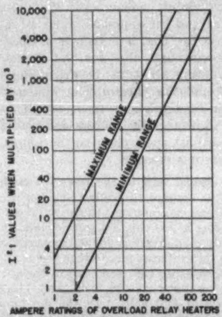

Figure 6. Range of permissible I^2t values which may be found in overload relays

4. If a slow interrupting means is used to protect against short circuits, the starter may be damaged, and the personnel may be injured.

The logical conclusion is that fuses are the only satisfactory available means to protect fully the small-size starters of either the a-c or d-c types.

Reference

1. Thermal Co-ordination of Motors, Control, and Their Branch Circuits on Power Supplies of 600 Volts and Less, B. W. Jones. AIEE Transactions, volume 61, 1942, July section, page 483–7.

A-C Contactors for Aircraft

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MEMBER AIEE

Synopsis: Use of a-c power systems on aircraft presents the problem of providing switching means. Contactors with the desired characteristics and suitable size and weight have not been available. Analysis of operating conditions, contactor size requirements, and performance characteristics provide a basis of design of a group of contactors to meet the needs of present and projected aircraft. These designs can meet aircraft size limitations and be capable of handling the circuits under all foreseen normal and emergency requirements.

PRESENT and projected designs of aircraft have electric-system requirements which far exceed in capacity those used in the past. As the airplanes have become larger, the electrically operated auxiliaries have required more and more power. Early airplanes had six-volt d-c systems, but these were quickly found to be inadequate because of excessive drop in the conductors. In order to avoid the use of prohibitively heavy cables, the voltage was increased to 12 and finally to 24 volts. Power requirements have continued to rise since the last increase in voltage, and a point again has been reached where a change in the electric system is considered necessary. A number of possibilities have been suggested.[1] One of these is the use of a-c generators with constant-voltage output at constant frequency with most of the power being used as generated but with a portion of it being rectified for use with d-c instruments and in control circuits. Many points in favor of this system have been advanced.[2]

Use of alternating current makes it necessary to provide suitable switching equipment for controlling the circuits. Relays and contactors used in d-c systems are nearly all single-pole low-voltage devices and are not satisfactory for use on three-phase a-c systems. While all of the devices needed are available in commercial form, their design is such that they are not satisfactory for use in aircraft. Their size and weight is excessive, and they are not designed for use under the wide range of conditions normal to aircraft operation.

Contactor Requirements

Design of the contactors needed involves a study of the factors influencing their operation. These include characteristics of the power system; type of load and duty cycle; as well as temperature, humidity, pressure, vibration, shock, dust, and other conditions to which they might be subjected.

Magnetically operated relays and contactors are used for the following purposes:

1. To shorten the power-carrying lines by running them only to the point of use.
2. To make possible automatic-control functions.
3. To save space at the control stations by the use of small pilot switches.
4. To keep high voltage from control stations by using low-voltage control circuits.

Alternators, driven by the main engines, furnish the principal source of power. The system is 208 volts, 400 cycles, three-phase with grounded neutral. In one proposed arrangement the main bus is a loop with sectionalizing contactors, so arranged that a fault may be isolated and removed with minimum disturbance to the remainder of the system. The alternators feed this loop through latched contactors arranged to trip on a fault in the feeders or in the machines themselves. Loads are fed from the main bus either directly or through branch bus systems. Contactors for connecting the loads are required. In some cases some of the branch systems may be transferred to an auxiliary power supply, in which case transfer switches are used.

If it should prove to be impractical to operate the alternators in parallel on a common bus, it is proposed to supply some of the loads from each of the alternators with provision for transferring in case of emergency. Transfer switches of suitable rating are required to perform this function.

The latched contactors function as circuit breakers operated by separate protective relays not part of the contactor itself. Separation of the protective devices from the main contactor is made in the interests of simplification of the devices and in order to increase interchangeability, since the same contactor may then be used with various circuit protective relays to obtain different types of protection or with manual control for sectionalizing the system.

The greater part of the load consists of motors. All motors are intended to in-

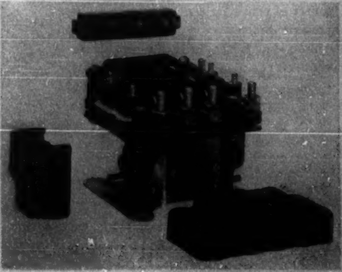

Figure 1. Three-pole single-throw contactor rated 25 amperes, 208 volts, 400 cycles

The covers and moving-contact assembly have been removed to show construction

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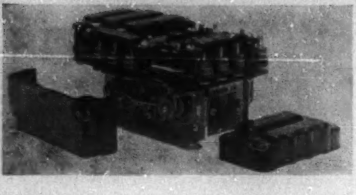

Figure 2. Three-pole double-throw 25-ampere contactor

corporate built-in overload protection, making provision of overload elements as part of the contactor or starting switch unnecessary.

Contactors of several types and ratings are required to perform various functions:

1. Single-pole single-throw for closing and opening single-phase load circuits—25 amperes continuous rating.
2. Single-pole double-throw for transferring circuits—25 amperes continuous rating.
3. Three-pole single-throw for closing and opening three-phase load circuits—5, 25, 50, and 100 amperes continuous rating.
4. Three-pole double-throw for reversing, motor-starting, and transfer of circuits—5, 25, 50, and 100 amperes continuous rating.
5. Three-pole latched for main circuit and alternator disconnects and for circuit protection—120 amperes continuous rating.

In addition to carrying rated full-load current, contactors must be capable of opening and closing the inrush currents which will be obtained with various types of loads. Under emergency conditions, the stalled current of motors may have to be interrupted. Current requirement under these conditions has been established as ten times full-load rating.

Temperature, vibration, and acceleration requirements have been discussed at length in connection with other aircraft equipment. Experience has indicated that in addition greater attention needs to be given to enclosures and protection against the effects of high humidity and the entrance of dust.

D-C Control Circuits

Availability of direct current as a source of power for control circuits makes possible the use of d-c magnets in the contactors. This results in certain advantages:

1. Magnets can be smaller, lighter, and more rugged than if they were made for operation on 400 cycles alternating current.
2. Switches now being used for the control of circuits and contactors in d-c systems can be used, making possible instrument panel layouts similar to those used in d-c systems.
3. Replacement problems are simplified.
4. Many of the present control schemes are usable.
5. High voltages which might be hazardous to operating personnel are removed from the pilot switches.

When automatic control from the a-c circuits is required, small rectifiers may be used to supply the d-c magnets.

Insulation

Use of three-phase circuits requires the use of three-pole contactors, introducing insulation problems in that it is necessary to insulate the a-c and the d-c circuits from each other and from ground, as well as to obtain phase isolation, all in the same device. In commercial practice, the idea of "phase spacing" is frequently synonomous with phase isolation, but in the design of aircraft contactors it is not practical to approach the problem from this point of view. It is not possible to space the poles sufficiently to obtain the required insulation distances without exceeding the space and weight limitations imposed on this apparatus. This problem is complicated further by the need for protection against the entrance of dust, a serious condition when operating on desert airfields, and by the effect of rarified atmospheres on insulation breakdown,[3] since the equipment must be suitable for operation at altitudes up to 40,000 feet. It therefore becomes necessary to obtain phase isolation by providing suitable barriers between poles and to provide an enclosure to prevent the entrance of dust. One of the principal causes of failure in circuit-interrupting devices is flashover between poles due to the presence of ionized gases. There are several ways in which this problem may be overcome:[4]

1. Sufficient space may be provided between poles to limit the ion concentration to a value at which flashover does not occur, a method which is not economical as to space and weight.
2. A sufficiently long path may be provided between poles in which the gases are in contact with cold surfaces so that deionization takes place.
3. The contacts may be placed in closed pockets which prevent the gases from coming into contact with parts of opposite polarity. Provision for venting the gases while maintaining a suitable dust seal may be required.

The last method is the only one which permits the contacts to be sealed to prevent the entrance of dust or other foreign material which might interfere with operation or which would cause insulation failure. It has the disadvantage that, in order to obtain the best space factor in the switch design, the arc is in close proximity with the insulation material. Failure may occur because of insulation breakdown, unless a material is used which has high resistance to arc tracking. Melamine formaldehyde insulation materials have this property and provide a suitable answer to this problem.

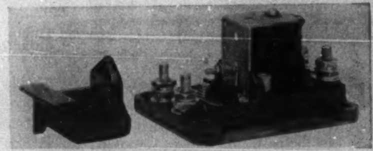

Figure 3. Single-pole single-throw 25-ampere contactor

Arc Interruption

The contactors must be capable of making and breaking the current which may exist under any condition encountered in operation. Under normal conditions, this may be the rated current or less, or the current at the instant the contacts close may be much higher, dropping quickly to the steady current. The contacts therefore must be capable of making ten times the rated current and breaking rated current as a normal condition, that is, for the life of the contactor. Under emergency conditions, the contacts may be called upon to open the same high current for a limited number of operations in order to clear faulty circuits or apparatus. In general, no difficulty is experienced with alternating current in breaking high current at the voltage used on these systems, even with relatively small arc gaps. The gap is determined by the distance required to prevent flashover at the maximum voltage used for insulation testing, in this case 2,500 volts at sea level and 1,000 volts at 40,000 feet altitude. Breaking high current may damage the contacts so as to reduce their life considerably or, in extreme cases, cause failure when the switch is again closed. Closing of the circuit presents a more serious problem, because, when closing on high currents, the contacts are liable to weld. Factors involved in this case are the current on which the contacts close, the material or materials of which the contacts are made, and the amount of arcing obtained during closure—the latter being determined by the mass of the contacts, their velocity and the forces involved: that is, the initial contact pressure and the magnet pull. The design must be such as to obtain the proper relationship of these factors to limit the rebound to as small a value as possible. Use of the contact materials which are not readily welded offers many possibilities, but care must be exercised to keep their resistance low. If the contacts are to be enclosed in relatively tight chambers for obtaining phase isolation and for protection against the entry of foreign materials, the enclosure must be made sufficiently strong to prevent breakage under the relatively high pressures built up when the arc is broken, or some provision must be made to permit the pressure to be relieved.

Arrangement

Two general contact structures are available. The moving contacts may be carried on a pivoted arm which is in turn operated by the magnet. In this form, the contacts are generally single break and have flexible connections to carry the current to the moving part. For high current values the connections must be quite large and heavy, so that in order to retain flexibility they are made relatively long. It was thought that a better choice for the larger-size contactors would be the use of bridging-type contacts. Figure 1 shows the arrangement of the contacts and terminals and is typical of three-pole contactors rated 25 amperes and higher. The contact cover and the terminal covers have been removed and the moving contact assembly lifted out. All stationary contacts are fastened to a molded insulating board with barriers to separate the phases. The moving contacts with their springs are carried in a sliding insulator assembly which is guided in a slot formed by the barriers provided between the stationary contacts. The barriers are designed to fit into recesses in the contact cover in such a manner that each contact is an individual chamber formed by the barriers, the slider, and the cover. While the closure between chambers is not absolutely sealed, the connecting spaces are very narrow and of such length that the gases are deionized before passing from one to the other. A seal against the entrance of dust is obtained by use of a compressible heat-resisting gasket between the edges of the cover and the base. Covers are provided over the terminal screws to prevent accidental contact by personnel or by objects which might fall across them. The sliding contact member is actuated by a pivoted lever, one end of which fits into the sliding bar and the other end of which is connected to the solenoid plunger. The contact bar and the solenoid plunger form a balanced structure which has essentially the same mass on each side of the pivot and is therefore unaffected by acceleration forces, making the device insensitive to shock and vibration. On double-throw contactors two similar contactors are mounted side by side on a common base as shown in Figure 2. A pivoted mechanical interlocking bar prevents one plunger from pulling in to close the contacts associated with it if the other plunger is already in its sealed position. Suitable covers with sealing gaskets protect the mechanism from mechanical interference and the entrance of dust.

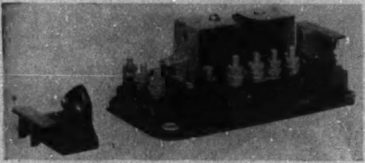

Figure 4. Three-pole double-throw five-ampere contactor

The smaller-rating contactors are readily adapted to the pivoted armature construction and accordingly are made in this form. Figure 3 is typical of the construction used and shows a 25-ampere single-pole single-throw contactor. A single bridging contact is used in this case, whereas on the three-pole contactor single-break finger-type contacts with flexible connectors are used. Double-throw contactors consist of two contactors mounted on a common base and mechanically interlocked to prevent simultaneous operation. Figure 4 shows a five-ampere three-pole double-throw contactor.

Contacts of the latched contactor are of the bridging type. The arrangement is similar to that used on the larger three-pole switches. The closing magnet consists of a solenoid which pulls the contacts into the closed position where they are held by a latch operating on the lever which moves the contact bar. The magnet coil is disconnected as soon as the contacts reach their sealed position by means of a single-pole double-throw switch operated from the contact bar. Sufficient overtravel of the contacts beyond the normal wear allowance is provided, so that the coil disconnect switch does not operate until after full latch engagement is obtained. This must be done to prevent the possibility of failure of the contacts to latch under low coil voltage. The coil switch also is connected in the circuit of the trip coil so that, when the main contacts are in the closed position, the trip coil may be energized to permit the main contacts to open. Intermittent-duty coils are energized only long enough to close or open the main contacts. In order to prevent "telegraphing," the operating and latch mechanism is arranged to prevent the

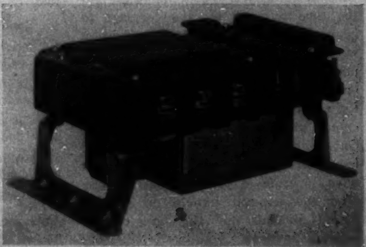

Figure 5. Three-pole latched contactor

Rated 120 amperes continuous with 4,000-ampere short-circuit capacity

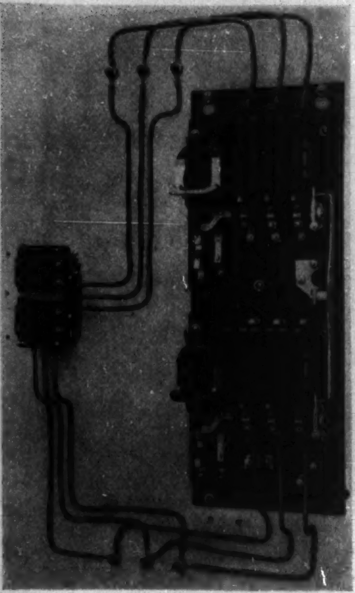

Figure 6. Three-pole double-throw aircraft contactor and comparable commercial contactor

closing magnet from closing the contacts following energization of the trip magnet until the closing-coil circuit has been opened. The contactor is "trip-free," that is, it cannot be held closed when the trip circuit is closed. Additional contacts are provided for electrically interlocking other circuits. Figure 5 shows the construction used in this contactor.

In order to protect other apparatus in the circuit, the fuses and the latched contactors must be co-ordinated in design to provide the proper protection from each under fault conditions. Accordingly, a specification has been established requiring that complete current interruption take place within 0.01 second after the trip impulse has been initiated. This time includes the arcing time and the time required for the contacts to begin to part. The worst condition which might be encountered is on single-phase where the arc might persist for one-half cycle plus the time required for the contacts to reach a gap great enough to prevent the arc from restriking. On 400 cycles this time will be something less than 0.002 second. The contacts must separate in 0.008 second or less after the trip-coil impulse is started. Results indicate that this time can be met satisfactorily.

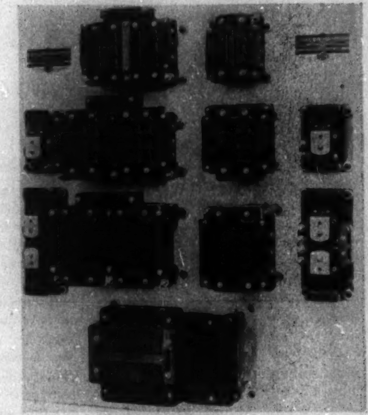

Figure 7. Various sizes and types of contactors required on large aircraft with a-c electric systems

Summary

Requirements for aircraft contactors may be summarized briefly as follows.

1. Small size and low weight.
2. Resistance to vibration and shock incident to their use on aircraft.
3. Ability to operate under a wide range of temperature, humidity, and pressure.
4. Enclosures to exclude dust from contacts and mechanism.
5. Ability to handle normal and emergency load conditions.
6. Confinement of arc within the device.

Analysis of these requirements and design to obtain maximum utilization of materials enables contactors to be built which are much smaller and lighter than their commercial counterparts. This is illustrated in Figure 6 which shows a 50-ampere three-pole double-throw aircraft contactor and a commercial contactor of the same rating. It should be noted that the commercial device is open whereas the aircraft contactor is completely enclosed.

Figure 7 shows all of the contactors designed to meet the needs of aircraft a-c electric systems. They include single-pole single- and double-throw contactors rated at 25 amperes; three-pole single- and double-throw contactors for 5, 25, 50, and 100 amperes; and a 120-ampere three-pole latched contactor or circuit breaker. Performance in each case meets the anticipated conditions which might be encountered in a-c systems used on aircraft and, with the exception of life, which for aircraft applications is much lower than required in commercial applications, either meets or exceeds the conditions required on the equivalent commercial contactor.

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Industrial Control—Shunted-Armature Connection for a D-C Shunt Motor

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THERE are many operations in applications where d-c shunt motors are used where it is necessary to slow down the rotating system in order to perform certain functions. Many typical examples are found in machine-tool work, such as cross-feeding of a tool, milling-cutter slow-down for sharp corners, accurate positioning of tool to work, slow-down of a grinding wheel for dressing. Other examples are found in the slowing down of a printing machine for the purpose of matching colors and occasionally to obtain a threading speed in a textile range drive.

There are three common methods by which these slow-down speeds may be obtained:

1. Resistance inserted in series with the armature is a method of slowing down a motor but has the disadvantage that the speed will vary considerably for slight changes in the torque demanded by the load, so that this method has poor speed regulation.

2. Variable-voltage control is a circuit in which the generator field is adjusted to vary the armature voltage, while the motor field is left constant. This method gives a range of speeds up to basic speed with good speed-torque regulation and good efficiency but is inherently too costly for intermittent applications such as were mentioned previously.

3. If resistance is put in parallel with the armature in addition to series line resistance, a method of control known as the shunted-armature connection is given. This method provides good speed regulation at low initial cost and is ideally suited for short-time or intermittent slow-downs, despite the decreased efficiency due to losses in the shunt resistance. The method is in effect a potentiometer connection (see Figure 1).

Scope of Paper

This paper presents a simple graphical method of obtaining the required shunt and series resistances for the shunted-armature connection to obtain any desired speed at a specified value of torque and also to give a predetermined no-load speed. An analytical verification of the graphical method is also presented.

The method is outlined for the cases of a constant-speed motor and an adjustable-speed motor.

Nomenclature

I = line current in amperes
V = line voltage in volts
R = series resistance in ohms
S = shunt resistance in ohms
E = back electromotive force in volts
I_s = current through shunt resistance
I_a = armature current in amperes
R_a = armature resistance in ohms
N = speed in rpm
E_o = back electromotive force in volts when $I_a=0$

All lower-case letters refer to the per unit value of the aforementioned quantities.

T = torque in foot-pounds
$\mathbf{T}$ = per unit torque

The subscript FL refers to the full-load condition in the motor. The subscript NL refers to the no-load or running-light condition when full voltage V is applied.

$R_o=V/I_{FL}$ = base or normal ohms
n_o = rpm in per unit when $i_a=0$

Analysis

Constant-Speed Motors

Application of Kirchoff's laws to the circuit shown in Figure 1 yields the equations,

$$I=I_a+I_s \tag{1}$$

$$V=IR+E+I_aR_a \tag{2}$$

and

$$I_sS=E+I_aR_a \tag{3}$$

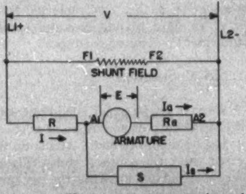

Figure 1. Elementary diagram for the shunted-armature connection showing shunt and series resistance

If equations 1, 2, and 3 are solved simultaneously, a relationship between the armature current and the counter electromotive force is given as a function of the circuit resistances. The resulting equation is

$$I_a\left[R_a\frac{(S+R)}{S}+R\right]=V-E\frac{(S+R)}{S} \tag{4}$$

The intercepts of equation 4 are given by putting $I_a=0$ and $E=0$. They become:

$$I_a=\frac{V}{R_a\dfrac{(S+R)}{S}+R} \tag{4a}$$

and

$$E_o=V\frac{S}{S+R} \tag{4b}$$

Equation 4 represents a straight line on the axes of armature current as the ordinate and counter electromotive force as the abscissa.

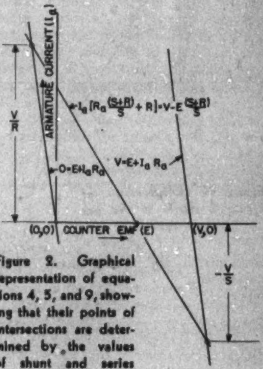

Figure 2. Graphical representation of equations 4, 5, and 9, showing that their points of intersections are determined by the values of shunt and series resistance

Let equation 4 be solved simultaneously with the motor equation,

$$V=E+I_aR_a \tag{5}$$

to find the point of intersection.

Substitution of $E=V-I_aR_a$ from equation 5 into equation 4 gives

$$I_a\left[R_a\frac{(S+R)}{S}+R\right]=V-(V-I_aR_a)\frac{(S+R)}{S} \tag{6}$$

which reduces to

$$I_aR=V\left[1-\frac{S+R}{S}\right] \tag{7}$$

so that, at the point of intersection,

$$I_a=-V/S \tag{8}$$

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Now, if equation 4 is solved simultaneously with the straight-line equation,

$$0=E+I_aR_s \qquad (9)$$

the point of intersection becomes

$$I_a=V/R \qquad (10)$$

This shows that, if equations 4, 5, and 9 are plotted on the same co-ordinates of I_a and E, their points of intersection are determined by the values of shunt and series resistance. These relationships are shown in Figure 2.

PER UNIT SYSTEM

Let line voltage V be 100 per cent or base voltage, full field current I_{FL} be base current, and no-load speed N_{NL} be base speed.

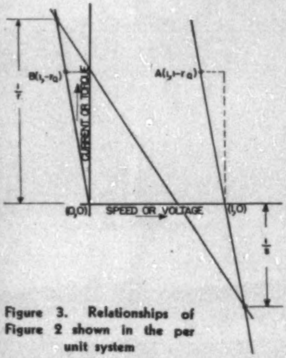

Figure 3. Relationships of Figure 2 shown in the per unit system

Since

$$T=c\phi I_a \qquad (11)$$

and

$$E=K\phi N \qquad (12)$$

then, by eliminating ϕ from equations 11 and 12,

$$T=\frac{c}{K}\frac{EI_a}{N} \qquad (13)$$

and

$$T_{FL}=\frac{c}{K}\frac{E}{N}I_{FL}=\frac{c}{K}\frac{V}{N_{NL}}I_{FL} \qquad (14)$$

since the back electromotive force equals line voltage at no-load speed. Therefore, base torque is full-load torque which is consistent with the aforementioned base quantities of voltage, current, and speed.

In this per unit system,

Per unit volts $e=E/V$

Per unit current $i=I/I_{FL}$

Per unit speed $n=N/N_{NL}$

Per unit torque $\text{T}=T/T_{FL}$

Also,

Normal ohms $R_o=V/I_{FL}$

so that

Per unit ohms = ohms/R_o

If equations 11 and 12 are put into the per unit system,

$$\text{T}=i_a \qquad (15)$$

and

$$e=n \qquad (16)$$

Figure 3 is obtained by transposing Figure 2 into the per unit system and using torque and speed as co-ordinates alternately with current and voltage as permitted by equations 15 and 16.

Equations 5 and 9 in the per unit system become

$$1=e+i_ar_a \qquad (17)$$

$$0=e+i_ar_a \qquad (18)$$

If i_a is put equal to unity in equations 17 and 18 to find the co-ordinates of two points A and B in Figure 3, $A\ (1, 1-r_a)$ and $B\ (1, -r_a)$ are yielded, so that equations 17 and 18 are determined only by the value of internal resistance in per unit at the unity-current point.

Equation 4 in per unit is

$$i_a\left[r_a\frac{(s+r)}{s}\right]+r=1-e\frac{(s+r)}{s} \qquad (19)$$

When i_a equals zero, equation 19 becomes

$$e_o=\frac{s}{s+r} \qquad (20)$$

From equation 4b:

$$\frac{S}{S+R}=\frac{E_o}{V}=\frac{E_o}{E_{NL}}=\frac{N_o}{N_{NL}} \qquad (21)$$

Now,

$$\frac{E_o}{V}=e_o$$

and

$$\frac{N_o}{N_{NL}}=n_o$$

so that $e_o=n_o$ from equation 21, and the intercept on the x axis is

$$n_o=s/(s+r)$$

Equation 19 is then easily plotted from the given data of one required speed-torque point and the no-load speed.

Graphical Solution

A sheet of graph paper is laid out with ordinate as current and abscissa as speed varying from zero to one. At the unity-current point, the points B and A (Figure 3) are plotted by knowing the per unit internal resistance of the motor. Through these points and the (0, 0) and (1, 0) points, respectively, straight lines are drawn. From the given data of a speed-torque point (i_a, n) in per unit and the per unit no-load speed required (n_o), a straight line is drawn. The two points of intersection of these three straight lines yield the reciprocals of the shunt and series resistance in per unit.

EXAMPLE

A machine driven by a ten-horsepower 230-volt 1,150-rpm d-c shunt motor must be slowed down for a short time, so that the shunted-armature connection is indicated. The stated requirement is that the slow-down shall be 45 per cent of the full-load motor speed, and that the speed regulation shall be such that the speed will not exceed $66^2/_3$ per cent of the full-load speed, if all load is removed from the motor shaft. It is given that the load demands 34 per cent of full-load torque, referred to the ten-horsepower motor, at 45 per cent of the motor-rated speed.

Since 100 per cent or per unit speed has been defined as the no-load speed of the motor, the full-load speed can be obtained by subtracting the per unit internal drop from unity. For this size motor the internal resistance is usually taken as ten per cent normal ohms, so that points A and B can be plotted on Figure 4.

The lines drawn through these points and the points (1, 0) and (0, 0) represent the motor characteristics with no added shunt or series resistance.

The 45 per cent speed referred to the no-load speed becomes:

$$0.45\times0.9=0.405$$

while the $66^2/_3$ per cent speed becomes:

$$0.66^2/_3\times0.9=0.6$$

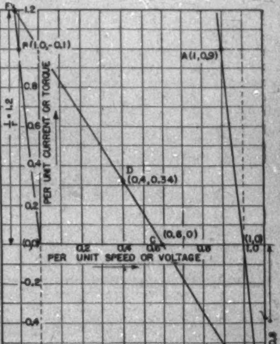

Figure 4. Graphical solution of example presented in the text

This translation of reference speeds from the full-load to the no-load base permits the plotting of the operating points in the per unit system as points *C* and *D*, Figure 4. A line drawn through these two points and extended will intersect the motor curves at points *F* and *G*. For this particular solution, the ordinates of the points of intersection are (+1.2) and (−0.8). The reciprocal of these two values gives the per unit ohms of the series and shunt resistances, respectively.

Therefore,

$$r = 1/1.2 = 0.833$$
$$s = 1/0.8 = 1.25$$

With 100 per cent ohms equal to line voltage *V* divided by full-load current I_{FL}, the necessary resistances to meet the preceding requirements are:

$$R = 230/39 \times 0.833 = 4.91 \text{ ohms}$$
$$S = 230/39 \times 1.25 = 7.37 \text{ ohms}$$

Adjustable-Speed Motors

A similar graphical construction can be used to determine the correct resistances for use with adjustable-speed motors. For these motors, load data are usually given with reference to the highest horsepower which occurs at the highest speed. To obtain speed control between highest- and lowest-rated speed, the field current is varied. For speeds below the lowest-rated speed, the armature-shunt connection is used. Therefore, if speeds are referred to this lowest-rated speed as base speed and base torque is full field torque, the graphical solution is identical with that for constant-speed motors. For example, assume that an application using a $7^1/_2$/10-horsepower 500/1,000-rpm, 230-volt d-c motor must be controlled to give 250 rpm at 80 per cent torque. This latter figure refers to the high speed or weak field torque and is therefore 40 per cent of full field torque. Accordingly, the point which corresponds to *D* in Figure 4 is plotted at

$$\frac{250\ (0.9)}{500} = 45 \text{ per cent speed and } 40 \text{ per cent torque}$$

Specification of the required speed with no load on the motor shaft, or specification of the speed regulation determines the second point, so that the straight line can be drawn as before.

Conclusion

In many applications the torque demanded by the load is not known very accurately, although the required speed is well defined. The application engineer can compensate for the inaccuracy with which the load torque is known by using the shunted-armature connection and by so choosing a value of shunt resistance to give a good speed regulation. Without a graphical solution, simultaneous equations must be solved analytically. The method previously presented is particularly useful because of its simplicity in obtaining a particular solution as well as determining families of curves that show the effect of shunt and series resistance on the speed-torque characteristics.

Possibilities of Heat Pumps for Heating Homes

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Synopsis: Heat pumps are refrigerating systems in which the principal interest is in the heat given off by the condenser. A portion of this comes from the electric input to the compressor and the remainder from cooling an external source of heat, usually outdoor air. Heat output is several times the equivalent of electric input, and these systems offer possibilities of fuel economy beyond anything possible with fuel-burning equipment. These systems can be used for all-year air conditioning, but the heat requirement dictates larger equipment than necessary for cooling only. Electric-load requirements are attractive, but further development is necessary and will be dependent on the promotion of summer air conditioning for homes.

SOME 92 years ago Lord Kelvin first called to our attention the fact that refrigerating systems are reversible, and that they have some unusual and interesting characteristics when operated as heating systems. Interest in the idea remained very low until about twenty years ago, when the development of the domestic electric refrigerator and commercial refrigeration for food preservation began to make engineers think of summer air conditioning for homes and buildings as something more than just a far-off dream. Since then interest, as judged by technical publications, has steadily increased, and a few actual installations have been made. Practically all of the installations were made before the war and nearly all of them are in successful operation today.

To predict that in the next ten years a substantial proportion of our homes will not be heated by heat pumps seems rash, in view of the rapid development we have already seen in other types of equipment for the home. Therefore, it would appear to be of interest to analyze the prospects as they exist today somewhat sympathetically, even though the way in which widespread use may be obtained is not yet clear.

Description of a Heat Pump

The heat which a domestic refrigerator absorbs from its cabinet through its evaporator does not just disappear. By means of a mechanical compressor, the temperature of the absorbed heat is raised to the point at which the heat can be transferred to the air outside of the cabinet through its condenser. In so doing it adds to the absorbed heat the heat equivalent of the work done by the compressor, and the sum of the two quantities of heat result as heat added to the room air surrounding the cabinet. When the reason for using the refrigerator is to take advantage of the lowered temperature inside the cabinet, it is called a refrigerator. When the primary object is to heat the surrounding room, it is called a reversed-cycle refrigerator or a heat pump. The type of equipment used in either case is the same. In the case of the heat pump heating a home, instead of placing the evaporator inside of an insulated cabinet, it is more effective to place it in the outdoor air or in a supply of water that can be cooled, and the condenser is usually in the form of a finned coil, with a fan blowing the heated air from the coil through ducts to the house. In any case, the heating of the house air and the simultaneous cooling of the outdoor air or water supply are fundamental to the heat pump, even though the outdoor air being cooled may be at a temperature well below zero.

Figure 1 shows a simplified schematic diagram of a direct-expansion heat pump. When operating in the winter time for heating, air from the house is forced through the condenser, where it is heated sufficiently to satisfy the heat loss of the house. Air from outdoors is forced through the evaporator, where it is cooled sufficiently to provide its contribution of heat to be transmitted to the house.

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The liquid refrigerant inside the evaporator is boiled by the heat from the outdoor air at a boiling pressure and temperature sufficiently below the outdoor-air temperature to effect the necessary heat transfer. The compressor draws the refrigerant gas from the evaporator and discharges it to the condenser at a high enough pressure and temperature so that it will condense and transfer its heat to the outgoing house air stream. The expansion valve maintains the pressure difference between condenser and evaporator by regulating the flow of refrigerant through the system. To use this same system for cooling the house in summer, the outdoor air and house air streams are interchanged with dampers, or an equivalent result is accomplished by other means.

If all losses are neglected, the heat output from the condenser of this system is equal to the heat input to the evaporator plus the electric input to the compressor motor. Since the outdoor air is free, the efficiency of the system is the heat output divided by the heat equivalent of the electric input, and because this is always greater than 100 per cent, it is called a coefficient of performance. The electric input multiplied by the coefficient of performance gives the heat output to the house.

Efficiency

The relative proportion of the heat obtained from electric input and from outdoor air depends primarily on the indoor and outdoor temperatures. This is quite different from a fuel-burning furnace, in which efficiency is substantially independent of outdoor temperature. As in the case of a steam-power plant, the heat pump is a heat engine absorbing heat at one temperature level and discharging it at another; the principal difference is that a steam-power plant delivers mechanical work from the engine, whereas the heat pump absorbs it. Both follow a Carnot cycle in the theoretical ideal, and the theoretical ideal efficiencies are similar in form, that for the heat pump being

$$\text{Coefficient of performance} = \frac{T_1}{T_1 - T_2}$$

where T_1 is the house or condenser temperature (both being the same in the ideal) and T_2 is the outdoor or evaporator temperature. Temperatures in this case must be measured on the absolute scale. Curve *A* in Figure 2 shows the coefficient of performance for a perfect Carnot engine heat pump maintaining 70 degrees Fahrenheit in a house at various outdoor temperatures. It will be noted that with a heat pump at 20 degrees Fahrenheit outdoor temperature it is theoretically possible to obtain 10.6 times as much heat in the house as would be possible by using the same power input in electric resistance heaters. In addition to a perfect heat engine this would require condenser and evaporator heat-transfer surfaces sufficiently large to transfer the heat without appreciable temperature differences, and correspondingly large air flows in the house and outdoor air circuits. Sizing the heat-transfer surfaces and air flows to values that would be considered practical in a summer air-conditioning system of similar proportions would give curve *B* in Figure 2, where the refrigerant condensing temperature has been increased and evaporating temperature decreased a total of 60 degrees Fahrenheit. This would permit delivery of heated air to the house at a temperature between 105 to 110 degrees Fahrenheit, which is considerably lower than would be used with a fuel-burning warm-air heating plant, but not enough lower than usual discharge temperatures from a normal year-round residential air-conditioning system to have a prohibitive effect on the cost of fans and duct work.

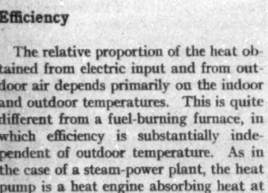

Figure 1. Schematic diagram of elements of a heat pump

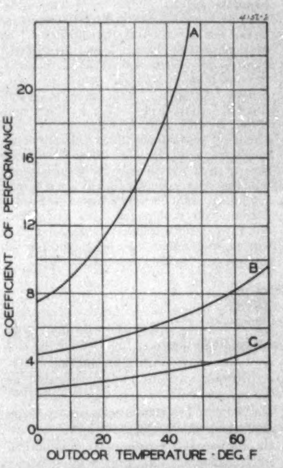

Figure 2. Efficiency characteristics of a heat pump maintaining a house at 70 degrees

Curve *C* in Figure 2 shows coefficients of performance that can be obtained with refrigeration equipment available today. This curve differs from curve *B* in that proper allowance has been made for the use of Freon 12, instead of a perfect refrigerant, for mechanical and other inefficiencies of equipment and for power required by auxiliaries such as fans. The performance indicated by this curve is believed to have been reached by installations now in operation using outdoor air as a source of evaporator heat. It will be noted that in climates such as Pittsburgh or Philadelphia, where less than three per cent of the requirements of the heating season occur at below 20 degrees Fahrenheit outdoor temperature, an average coefficient of performance for the whole heating season of better than three to one can be reasonably expected.

Fuel Economy

At about 52 degrees Fahrenheit outdoor temperature, the realizable coefficient of performance reaches four to one. The best steam-power generating plants today have an over-all thermal efficiency of approximately 34 per cent. Hence, even with a 15 per cent allowance for transmission losses between the generating station and the home, the heat pump will deliver at the 52 degrees Fahrenheit outdoor condition 116 per cent of the heat that was in the fuel burned at the generating station for supplying energy to the heat pump. Under favorable conditions the heat pump can pick up from its outdoor air more than all of the losses from the system, including the heat rejected to its cooling water by the generating station. At first glance this might appear to offer encouragement to the seekers of perpetual motion machines, but a second glance reveals that, while we have increased the number of Btu available at the heat-pump condenser over what we started with in the fuel, its temperature level would not be attractive to the central-station operator as a source of mechanical work. The heat pump effects a favorable exchange of high-grade electric energy for a greater quantity of low-grade heat energy, and it is well known that the same favorable rate of exchange cannot be obtained in the opposite direction. However, the heat output of the heat pump is quite suitable for heating homes, and even at a three to one season-average coefficient of performance and 15 per cent electrical transmission losses, it gives 87 per cent over-all fuel economy. This is substantially better than can be obtained with the best domestic automatic fuel-burning equipment, and more than 50 per cent better than a good hand-fired domestic coal furnace will give. As time goes on undoubtedly more efficient central stations and more efficient heat-pump equipment will be developed, and it is not impossible to imagine that heat pumps may some day be required just from the point of view of conservation of our national fuel resources. Curve *A* in Figure 2 shows plainly that the development of this type of system is not limited to 100 per cent fuel economy.

Of course, fuel utilization efficiency is not so interesting to the operator of a heat pump as is the annual operating cost in dollars. Here there can be a considerable variation of opinion, because there are no reliable statistics available on the actual average operating efficiencies of the various kinds of fuel-burning domestic heating plants. If we assume hand-fired coal furnaces at 55 per cent efficiency, and oil and gas automatic furnaces at 75 per cent efficiency, they would compare as follows with a heat pump averaging 3.0 coefficient of performance and using power at one cent per kilowatt hour. The heat pump would have the same operating cost as:

1. Coal at $14 per ton.
2. Oil at ten cents per gallon.
3. Manufactured gas at 40 cents per thousand cubic feet.
4. Natural gas at 80 cents per thousand cubic feet.

This is approximately the picture as it exists today for a heat pump constructed with general-purpose refrigeration equipment in a climate such as Pittsburgh and drawing its evaporator heat from the outdoor air. It can be improved substantially by circulating more house air at a lower temperature and using larger heat-transfer surfaces, thus increasing the first cost and departing from present limits of practice in year-round air-conditioning systems. It can be improved, also, by further development of the equipment, or by finding a higher-temperature source of heat for the evaporator than outdoor air.

The possibilities of higher-temperature heat sources have been well explored but possibly not exhausted. Most city water supplies are at a high enough temperature in winter to be usable as a heat source, but the cost of the water usually will exceed the power saving. The use of the heat of fusion of water by freezing ice presents the problem of disposing of tons of ice per day per home in very cold weather. The use of well water, where available for the cost of pumping, can give average coefficients of performance of higher than four to one, and has been used very successfully, but since the availability in the United States is very limited, this furnishes only a local solution to the problem.

Output Characteristics

Curve *A* in Figure 3 shows the output characteristic, calculated from test data, of a heat pump using a constant-speed $7^1/_2$-horsepower condensing unit with an outdoor air evaporator. In spite of the fact that the coefficient of performance or efficiency falls off at low outdoor temperatures, the power input, as shown in curve *B*, also falls off because of the decrease in circulation of refrigerant at low suction densities as the evaporator temperature and pressure fall. Curve *C* shows the requirements of a house needing 85,000 Btu per hour at zero degrees Fahrenheit outdoor temperature, which would be a three-bedroom house of average proportions, well insulated, in a climate such as Pittsburgh or Philadelphia. Such a house would be in the $8,000 to $10,000 price range at prewar levels. The $7^1/_2$-horsepower heat pump has capacity to heat this house down to 15 degrees Fahrenheit outdoor temperature. Rather than use a larger heat-pump system to take care of temperatures below 15 degrees Fahrenheit, a storage scheme could be used. The excess capacity of the system at temperatures above 15 degrees Fahrenheit would be used to heat water in a storage tank to a temperature of 100 to 110 degrees Fahrenheit, and when the outdoor temperature falls below 15 degrees Fahrenheit this reservoir of heat can be drawn upon to maintain the house. The size of storage tank that will be required will depend upon the severity of the longest cold spell below 15 degrees Fahrenheit to be expected at the location of the heat pump. In Pittsburgh, the size of the tank would be of the same order of magnitude as the fuel-oil storage tank that might be used if the same house were heated with an oil-burning furnace.

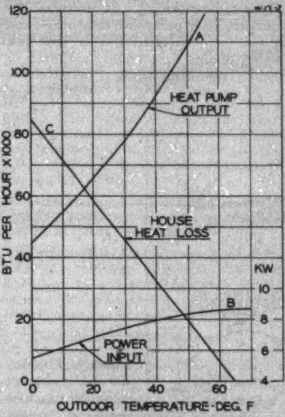

Figure 3. Output characteristics of a heat pump with constant-speed $7^1/_2$-horsepower compressor

Sources of auxiliary heat other than storage also can be used where advantageous. The city water-supply system is a convenient source in this case, because the total usage will be small enough so that the cost is not important, and the demand would come at a season when practically all city water systems have ample surplus capacity. A small auxiliary evaporator cooling this water would be called upon for less than three per cent of the integrated heating-season demand in the Pittsburgh climate.

From curves *A* and *C* in Figure 3 it is obvious that the constant-speed compressor gives far too much capacity at the higher outdoor temperatures. This is not desirable in any warm-air heating system, where it is likely to cause short cycling and excessive temperature fluctuation in the house. This type of system, therefore, requires a multispeed compressor motor or other means of capacity reduction, with the necessary controls to modulate the system output to the approximate requirements of the house.

A requirement of 85,000 Btu per hour to heat a house at zero degrees Fahrenheit is composed of conduction through walls and windows, and infiltration of outside air. In summer the indoor–outdoor design difference is typically only 16 degrees Fahrenheit, which reduces the Btu load to 19,400 Btu per hour. To this must be added the requirements for increased fresh-air supply, people, lights, solar radiation, and latent cooling of the fresh air and people. All of these can vary widely, but it can be said that the majority of insulated houses of this size can be air conditioned satisfactorily with a three-horsepower system giving from 35,000 to 40,000 Btu per hour under hot-weather conditions. Some houses with large glass areas exposed to the sun may require five-horsepower systems. This is typical of most parts of this country, and brings out the fact that few summer air-conditioning systems, designed as such for residential use, can be converted to heat pumps without adding capacity. Conversely, the cost of installing a heat pump in a residence involves more than just the extra cost of controls and reversing mechanism applied to a summer air-conditioning system. Whereas the capacity of the condensing unit may have to be increased in the ratio of $7^1/_2$ horsepower to three horsepower, the increase in size of heat transfer and air-handling equipment generally will be considerably less.

Initial Cost

Any authoritative discussion of installed prices of heat pumps would be hopelessly beyond the scope of a short exposition such as this. It would involve the effects of local codes and building-trades practices, methods of selling and installing, selection of installation materials, and all the other things that cause large variations in prices between one locality and another, and between two installations in the same locality when the system is assembled from individual pieces specifically engineered in the field to the requirements of a particular house. Nonetheless, some highly approximate estimated figures might be of value as an indication of relative costs compared to those of conventional systems. A warm-air oil-fired furnace for a new well-insulated house requiring 85,000 Btu per hour might be sold for $700 today in a medium-size city. This would include uninsulated duct work, grills, oil tanks, and all necessary items for a complete installation, with a minimum of accessories. If this installation had been purchased as a year-round system, the duct sizes would have been somewhat larger, parts of the duct work would have been insulated, and the added equipment might have included a three-horsepower condensing unit, an evaporator coil mounted in the supply-duct work, an evaporative condenser, refrigerant piping, cooling controls, refrigerant, and numerous smaller items. If the same method of estimating is used as in the case of the simple furnace, this system complete would be sold for $1,650. Again, if the same basis for estimating is used, a $7^{1}/_{2}$-horsepower heat pump with two-step capacity modulation, outdoor-air evaporator supplemented with a 1,000-gallon insulated water-storage tank for peak load, and automatic controls except for manual changeover from heating to cooling, would be sold for $2,600. The heat pump might in some cases be entitled to a credit of approximately $150 because it would not need a chimney. These figures are not represented as being either average or typical, but are prices that might be encountered in a climate such as Pittsburgh if the standard heating and refrigeration equipment available today were used. The cooling equipment would be selected and installed by the contractor in the field from various designs and sizes of compressors, fans, heat-transfer surfaces, and so forth. Obviously, whenever a volume is reached sufficient to justify pre-engineered packaged designs assembled in factories, the price of the complete system to the user would be considerably altered.

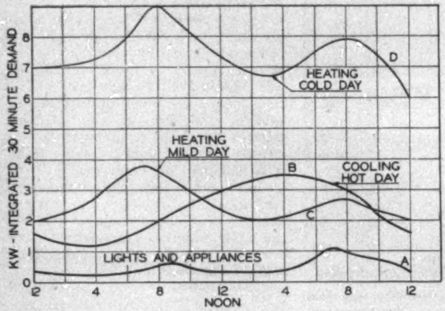

Figure 4. Kilowatt-demand requirements of a heat pump at various conditions

Energy Usage

Figure 4 shows the effect on electrical demand of installation of various types of systems in the typical six-room house in a Pittsburgh climate. Curve *A* is for reference, and shows the average hourly demand of an all-electric home, containing all electrical appliances except heating and air conditioning. It averages 360 kilowatt-hours per month total usage, and it will be recognized readily that few homes of this size actually approach or seldom exceed this figure. Curve *B* shows the demand of a three-horsepower summer air-conditioning installation in the same house on a hot day, when the outdoor temperature reaches 96 degrees Fahrenheit in the afternoon. Curves *C* and *D* show hourly demand curves for a heat pump in this house on a mild day and on a zero degree Fahrenheit day, respectively. If this house were heated with electric resistance heaters, the peak demand of the heating system would be 25 kw.

Estimating the average annual energy consumption of a heat pump is obviously a tedious undertaking, because the coefficient of performance is a function of outdoor temperature and the result is doubly dependent on the weather. However, approximate calculations for the average Pittsburgh climate, including both winter heating and summer cooling, give a total usage of 17,000 kilowatt-hours per year. If Pittsburgh represents an average climate in the United States, for which reason it was chosen here, then, even after all homes have reached a lighting and appliance usage of 4,000 kilowatt-hours per year, it would require installation of heat pumps in only 25 per cent of them to double the domestic electric load.

Conclusions

Quite apart from the residential possibilities of heat pumps, these systems should receive greatly increased attention in the postwar period because of their commercial and industrial applications. Many industrial applications will be found where there will be an economically advantageous use for both the heating and cooling that these systems provide simultaneously. Many industrial and commercial applications will be found where summer air conditioning is necessary, and where a high internal cooling load will make the cooling equipment adequate as a heat pump without increasing its size. Again, the availability of well water or process waste heat at a temperature too low for space heating but above outdoor temperature may make the heat pump, as an addition to a cooling system, far more attractive economically than in the case of the residential application.

If the increased interest is to carry over into the residential field and result in any widespread use, it is clear that further development work will have to be done. At present costs and performance it can be predicted safely that usage will be very limited except possibly in southern California and Florida, where the climate is much more favorable than in Pittsburgh. However, from the standpoint of first cost, it is also clear that the heat pump, when developed, will be attractive principally to those people who want and can afford to pay for over-all summer air conditioning of their homes and not for just one or two rooms. To date this market might be described as extremely small but promising. Equipment consisting of an electrically driven summer air-conditioning plant in combination with a fuel-burning furnace has reached a good stage of development, and application of it to residences is well past the pioneer stage. However, it has not received the promotion that manufacturers and utilities gave to the household electric refrigerator, for example, when it was introduced. Certainly endeavors will be made to promote it in the postwar period. The future development of the heat pump for residential use probably will depend upon the relative success or failure of these endeavors.

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Modern Cargo-Winch Control for Use on Victory Ships

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NONMEMBER AIEE

THE electric-driven cargo winces used to load and unload a modern cargo vessel are required to give fast efficient operation. Wartime conditions have made the time for loading and unloading cargo more important than ever. In addition to the need for optimum operating characteristics, the war has also brought about changes in the construction and assembly of the winches and their associated control. Every square foot of deck space is needed to carry cargo to the fighting fronts, and the space allotted to the winch must be kept to a minimum. In the past the winch, the control panel, and the resistor had been furnished separately, then mounted and wired on the ship. The time to install all this equipment on the vessel is now too long to keep pace with the accelerated shipbuilding program. The problem is being solved by furnishing the winch, the control, and the resistor, as a unit. This unit is interconnected so that very little wiring must be done on the ship and the installation time is materially reduced (see Figure 1).

In this paper the operating characteristics required for an electric-driven cargo winch will be discussed. The manner in which these characteristics may be obtained by the use of a properly designed motor control, and the problems encountered in incorporating control equipment in an enclosure mounted on the winch will be outlined.

Control Requirements for the Cargo Winch

Most cargo is handled by using two winches operating on a burtoning rig (see Figure 12 and appendix). This setup results in one winch hoisting the load and transferring the load to the other winch which lowers it. Either winch will hoist a load of a ton and a half at a speed of 290 feet per minute. Desirable operating characteristics for these winches are:

1. Speed characteristics suitable for use on a burtoning rig. The speed of one winch at light load must be matched by the other winch operating at full load.
2. Rapid acceleration and deceleration of the load to decrease the load travel time. Acceleration of the load is an important factor in placing the load under the hatch.
3. Extremely low speed, from 10 to 20 feet per minute, lowering to land heavy loads. This feature is important when lowering a load into a lighter. The lighter may be moving up to meet the load, by the action of a wave, which effectively increases the landing speed.
4. High hoisting and lowering speeds with an empty hook, approximately 400 feet per minute to give fast loading cycle.
5. Intermediate hoisting speeds at light loads are necessary to transfer the load smoothly from one winch to another. These intermediate hoist speeds also give better control of an empty hook.
6. Good jogging characteristics.

The desired cargo-winch operating characteristics may be obtained by the use of a d-c compound-wound motor. The proper connections of the motor to the line on the various operating points must be provided by the control equipment. These power-circuit connections for each point are shown, and the reasons why they are used are explained in the following.

First-Point Hoisting

This is a low torque point used to take the slack out of the cable and gears. The motor is connected to the line through a relatively large value of resistance, which limits the torque that may be produced on this point. In order to reduce the light-line speed a resistance is connected directly across the motor, inside the line resistance, which limits the no-load armature voltage. The circuit is shown in Figure 2.

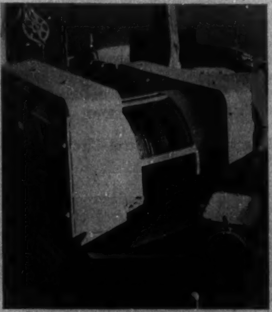

Figure 1. One-unit cargo winches installed on board ship

Second-Point Hoisting

A relatively low speed with full load is needed on this point. This may be obtained by using the same circuit as used on the first point and merely reducing the line resistance. This circuit gives a little greater light-line speed and produces enough torque to hoist full loads slowly. The circuit is shown in Figure 2.

Third-Point Hoisting

The load speeds must be increased on this point and there is no need to hold the light-line speed to a low value as the two intermediate speeds provided on the first two points are sufficient. The load speed can be increased by removing the motor shunt resistor which also permits the light-line speed to increase. The circuit is shown in Figure 2.

Fourth-Point Hoisting

This point gives greater hoisting speed which is obtained by cutting the value of line resistance.

Fifth-Point Hoisting

The higher speed on this point is obtained by connecting the motor directly to the line.

First-Point Lowering

Extremely low speeds with all values of load are desirable on this point properly to land heavy loads. In addition, the motor should be capable of paying out cable with no load on the hook. The low speeds may be obtained by using a compound-wound motor, as a shunt-wound machine with heavy field excitation. This is accomplished by connecting the series field to the line through a resistor and shunting the armature across the series field. If the value of line resistance is kept low enough, the motor will have sufficient breakaway torque to start a light line downward. The speed on this point is extremely low, even with the heaviest overhauling load. The circuit is shown in Figure 2.

Second-Point Lowering

The higher speed that is required on this point is obtained by weakening the

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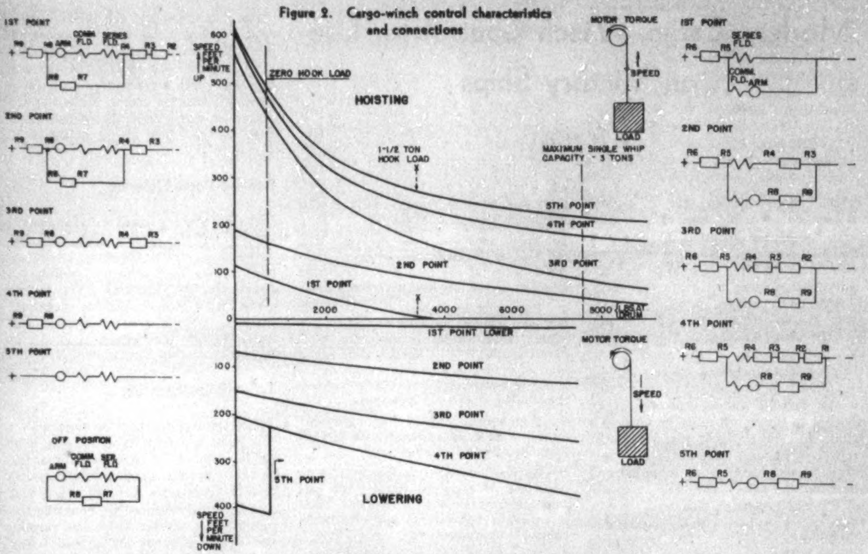

1ST POINT
R9
R8 ARM
COMM FLD.
SERIES FLD.
R4
R3
R2
R8
R7
2ND POINT
3RD POINT
4TH POINT
5TH POINT
OFF POSITION
ARM
COMM. FLD.
SER. FLD.
SPEED FEET PER MINUTE UP
600
500
400
300
200
100
0
SPEED FEET PER MINUTE DOWN
ZERO HOOK LOAD
HOISTING
1-1/2 TON HOOK LOAD
MAXIMUM SINGLE WHIP CAPACITY - 3 TONS
5TH POINT
4TH POINT
3RD POINT
2ND POINT
1ST POINT
2000
4000
6000
8000
LBS AT DRUM
1ST POINT LOWER
5TH POINT
LOWERING
MOTOR TORQUE
SPEED
LOAD
R6
R5
SERIES FLD.
COMM. FLD.
ARM
R1
R9

Figure 2. Cargo-winch control characteristics and connections

B. Hoisting power circuit diagrams

A. Cargo-winch speed–load characteristics for 50-horsepower motor geared to winch with 15 to 1 ratio

C. Lowering power circuit diagrams

motor-field excitation. A resistance is also inserted in the armature circuit, so there will be a little more variation in speed with load. This lines up the full-load speed on this point with the no-load speed on the third point. See Figure 2. This is important when burtoning a load, as one winch is handling full load and the other no load. The circuit is shown in Figure 2.

Third-Point Lowering

The increased speed on this point comes from a further reduction in field excitation, by the increased resistance in the series-field circuit. The rest of the circuit is the same as that on the second point. The full-load speed on this point lines up with the no-load speed on the fourth point, which is valuable for burtoning (see Appendix 1). The circuit is shown in Figure 2.

Fourth-Point Lowering

Another speed increase is obtained by a further reduction in series-field excitation. Full-load speeds on this point line up with the speed at no load on the fifth point.

Fifth-Point Lowering

A high light-line lowering speed to decrease the time to return the empty hook must be furnished on this point. This speed is obtained by reducing the field excitation to a low value.

In this case, the series field is entirely removed from the circuit, and the only field comes from the shunt field. This speed is extremely high, and it would not be safe to handle loads at this speed. For this reason, the connection is maintained only with light loads, and, if the hook is loaded, the control circuit automatically returns the motor to the fourth-point connections.

Off Position

An emergency dynamic-braking circuit, made up of the series field, the armature, and a resistor, is available in this position. This circuit automatically will provide a restraining torque for any overhauling load in case of power failure.

Description of Control-Circuit Problems

A number of interesting control-circuit problems was encountered in obtaining the desired motor power connections. Rapid response of the motor to the movement of the master-switch handle is needed to give good jogging characteristics. When the master-switch handle is moved, circuits are established to release the brake and to energize the motor. The pickup speed of the brake and the time required for the contactors to close determine the ease with which the load may be jogged. The brake pickup speed is increased by the use of a quarter-voltage coil on the brake, connected to the line through a resistor. This combination reduces the effective circuit inductance which cuts the time of the current buildup, and thereby increases the pickup of the brake. The contactors designed for this application pickup and drop out much faster than the ones previously used. These contactors are connected in such a manner that they pick up directly from the master switch and do not have

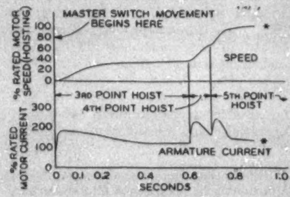


Figure 3. Acceleration curves

Hoisting from standstill to full speed with full load on the hook. Master switch thrown instantaneously from off position to fifth-point hoist

* Motor operation on fifth point is with weakened shunt field

to wait for sequencing from other devices.

Another function of the control is to make rapid acceleration of the load possible. This is important when lowering, as it permits the stevedore to swing a load under the hatch coaming by starting the load to swing and then paying out cable as it swings in the desired direction. The more rapid the acceleration, the more the advantage that may be taken of the swing to place the load in the desired position. Rapid acceleration in the hoisting as well as in the lowering direction reduces the total travel time. Very rapid load acceleration without exceeding maximum permissible torque is obtained by the proper spacing of the speed-torque characteristics in the hoisting direction, the addition of a timing relay to maintain operation on point three for a half a second, and the operation on point four for as long as it takes *3A* contactor to pick up through an interlock on *2A*. The current-versus-time and speed-versus-time curves for fast movement of the master switch are shown in Figure 3. Faster acceleration in the lowering directions is obtained by increasing the motor torque that drives the load downward. Gravity helps accelerate the loads in the lowering direction, and rapid release of the brake is important.

The control problem that requires more attention than all others is the establishment of a decelerating circuit that will smoothly slow down a load being lowered at high speed without causing poor commutation. The circuit must be laid out on the basis that a stevedore may be operating the winch at full load and full speed, and may then rapidly return the master switch to an intermediate or the off position. When the winch is lowering full load, the motor is acting as a generator with a very weak field and its counter electromotive force is equal to or slightly higher than line voltage. As the master switch is moved toward the off position, the resistance in the motor loop circuit is decreased and the counter electromotive

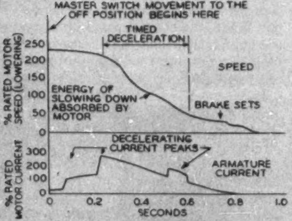
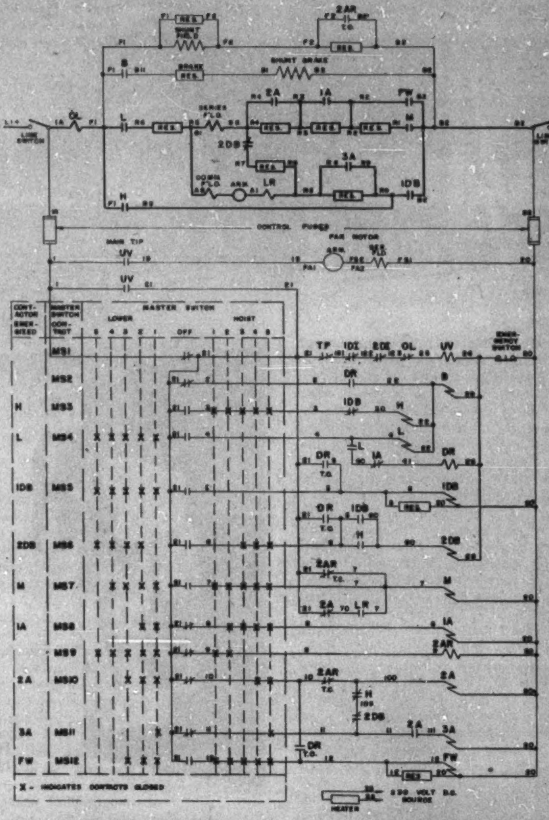


Figure 4. Test data

Deceleration from high-speed lowering with full load to standstill. Data from oscillogram taken during factory test on cargo winch



CONTACTORS			
NOMENCLATURE			SYMBOLS
M	LINE		⊣⊢ CONTACTOR TIP NORMALLY OPEN
H	HOIST	H-L, H-1DB MECHANICALLY INTERLOCKED	⊣/⊢ CONTACTOR TIP NORMALLY CLOSED
L	LOWER		SHUNT COIL
1DB			
2DB	DYNAMIC BRAKING		⊣⊢ NORMALLY OPEN INTERLOCK
1A	ACCELERATING		⊣/⊢ NORMALLY CLOSED INTERLOCK
2A			
3A			
FW	FIELD WEAKENING		
B	BRAKE		

RELAYS				
NOMENCLATURE		SETTING		SYMBOLS
UV	UNDERVOLTAGE	PICKUP-140 VOLTS D.C. ABOVE 60 VOLTS		SERIES COIL
DR	DECELERATING	0.5	SECONDS TO RELEASE	SHUNT COIL
2AR	ACCELERATING	0.5		T.O. NORM. OPEN CONTACT T.O. INDICATES TIME DELAY OPENING
OL	OVERLOAD	380	AMPS	
LR	LOAD RELAY	87-90 BELOW	AMP REV CURRENT LOWERING	T.O. NORM. CLOSED CONTACT T.O. INDICATES TIME DELAY CLOSING

DEVICE SEQUENCE CHART

DEVICE	LOWER					OFF	HOIST				
	5	4	3	2	1		1	2	3	4	5
UV	X	X	X	X	X	X	X	X	X	X	X
LR	X*										
H							X	X	X	X	X
L	X	X	X	X	X						
1DB	X	X	X	X	X						
2DB	X	X	X	X					X	X	X
M	X*	X	X	X	X		X	X	X	X	X
1A				X	X	X		X	X	X	X
2A					X	X				X	X
3A					X	X					X
FW			X	X	X		X	X	X	X	X
2AR	X	X	X	X	X	X	X	X			
DR	X	X	X								
B	X	X	X	X	X		X	X	X	X	X
X - INDICATES DEVICES ENERGIZED											

X* - DEVICES ENERGIZED ON OVERHAULING LOAD ONLY

LIMIT SWITCHES	
TP	THERMAL PROTECTOR
1DI	AIR DOOR INTERLOCKS
2DI	

Figure 5. Elementary diagram of cargo-winch control

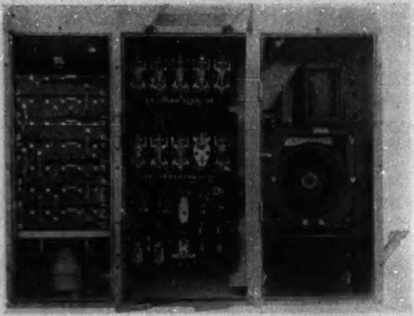


Figure 6. Front view of unit winch control with all doors open

force will circulate a large current through the motor armature and series field. If the loop-circuit resistance is cut out quickly before the load is slowed down and the counter electromotive force decreased, the current values will be large, and poor commutation will result. For this reason it is necessary to sequence the action of the contactors to give a time delay in reducing the resistance in the loop circuit. It is not enough to sequence the devices; a time-delay relay must provide prolonged operation with an intermediate value of resistance. This slows down the load, thereby reducing the counter electromotive force and the resulting current peak when the loop-circuit resistance is decreased. The current-versus-time and speed-versus-time curves obtained for rapid deceleration of full load are shown in Figure 4. The automatic-deceleration feature not only makes it possible to bring the load to rest as quickly as practical, but also permits plugging from the lowering to the hoisting direction. The deceleration of the load by the motor reduces the job to be done by the brake in stopping the load. On the winch-control circuit, the release of the electric brake is held off until the timed deceleration is completed. This prevents the brake from setting until the dynamic-braking circuit has reduced the speed at which the brake would be applied to approximately one half. This reduces the wear on the linings considerably.

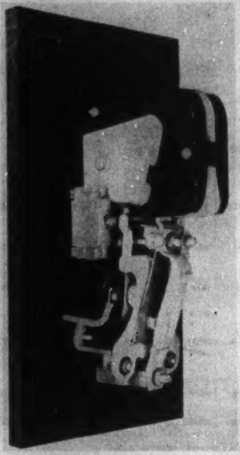


Figure 7 (left). Normally open contactor

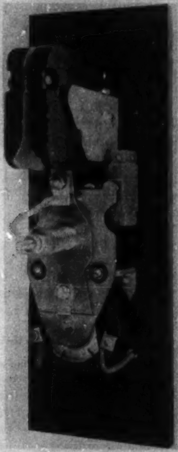


Figure 8 (right). Normally closed contactor

One other part of the winch-control circuit is the safety features provided. The use of a shunt-wound brake is necessary on this application, since the current distribution between the series field and armature circuit is not well suited for the use of a series-wound brake. The series brake usually is considered very safe, as it is located in the power circuit (between $S2$ and $R4$), and a failure in the supply sets the brake. Some of the safety features of a series brake can be obtained when using a shunt brake, if the coil of the brake contactor is connected in series through H or L coils. This prevents release of the brake if coils of either of these devices fail. The most serious failure which could occur would be that of the H contactor when hoisting, as the load starts to drop, and there is not as much tendency for the stevedore to return the handle to the off position. An emergency switch is provided for stopping the motor. The circuits are set up so that failure of H or IDB contactors de-energizes the coil of the normally closed emergency contactor $2DB$. This establishes the off-position dynamic-braking loop circuit. Another safety feature is the interlocking provided between the relay $2AR$ and contactor M. The relay $2AR$ weakens the shunt field when it is de-energized, thereby providing higher light-line speeds on the last three hoisting points. If this relay should fail to remain in the closed position in the lowering direction, the motor speed would be high. This is very important on the fifth point as the contactor M is opened, removing the series field, with a light load. The use of a normally closed interlock on $2AR$ connected to the coil of M prevents removal of the series field with a light load. The use of a normally closed interlock on $2AR$ connected to the coil of M prevents removal of the series field if $2AR$ is de-energized and the shunt field weakened. The control circuit with the safety features mentioned that gave the necessary power connection is shown in the elementary diagram (Figure 5).

Construction of Control Equipment

The specifications for cargo-winch control on the newer vessels require the control equipment with the exception of the master switch to be mounted directly on the side of the winch. In this location the equipment must be protected from the action of rain and waves, as well as take a minimum amount of space. The standardized Maritime Commission design for control equipment has the electric brake, the control panel, and the resistor located in separate compartments of a large waterproof enclosure on the

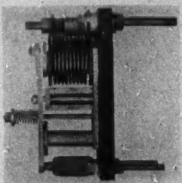


Figure 9. Load relay

side of the winch. See Figure 6. The case is constructed of $^3/_{16}$-inch steel, reinforced with partitions to give it increased strength. The control panels located in the middle compartment must be easy to service and relatively easy to remove if this should be necessary. This necessitates the panel being built in three sections which may be removed from the front of the enclosure. The weight and size of each panel is kept small by the use of a new-type marine contactor. The new normally open as well as the normally closed contactors have their armatures pivoted on synthetic-rubber bearings. The normally closed contactor is provided with an Alnico magnet to give rapid opening of the main contact tip. See Figures 7 and 8. Totally enclosed electrical interlocks are used with these new contactors. The relays are located on the bottom panels, and the load relay, which gives high-speed lowering with light load, is especially designed for this application. The relay is both small and lightweight. The device has a magnetic circuit made up of an Alnico magnet and a series coil carrying motor-armature current. See Figure 9. The design of the device is such that it gives positive snap action when the critical value is reached. A heater of 100-watt capacity is located in the panel compartment to keep the temperature above the outside ambient temperature, and prevents condensation of moisture.

The magnetic brake is solenoid-operated with the solenoid located above the motor shaft. This construction keeps the width of the brake compartment small. The brake designed for this application, shown in Figure 10, is a two-shoe brake. The brake resistor also is located in the compartment.

The power resistor (see Figure 11), which is located in the remaining compartment, is called upon to dissipate quite a bit of power, particularly on the slow-speed point. It was not possible to furnish a totally enclosed resistor in the available space because of the temperature rise of the resistor and case that would result. The only alternative was to provide forced-air ventilation for the resistor. The specifications require the resistor compartment to be watertight when the equipment is not being used and the ship is at sea. The openings used for air intake and outlet for resistor ventilation are provided with watertight doors. These doors are opened when the equipment is to be operated, and interlock switches connected to the doors prevent operation of the equipment unless the doors are open. The resistor units, boxes, and interconnections are built to operate, even though rain is being driven into the air openings. The entire front of the resistor compartment may be removed, if it is necessary to replace resistor units. All of the units are mounted in unit boxes. There are seven of these boxes, and each box slips into its own tray and is held in place by a clip at the back and two screws at the front. The interconnections between boxes are such that they may be removed from the front and the box taken out easily. The resistor ventilation is furnished by a propeller-type fan which is capable of delivering 1,000 cubic feet of air against a static pressure of $^3/_8$ inches of water. This fan is driven by a one-quarter-horsepower, 1,750-rpm series-wound motor. The series-wound motor being used to obtain maximum air delivery is located below the resistor. The fan actually delivers 1,500 cubic feet of air a minute, as the fan load is less than fan motor rating and the motor runs at 1,965 rpm.

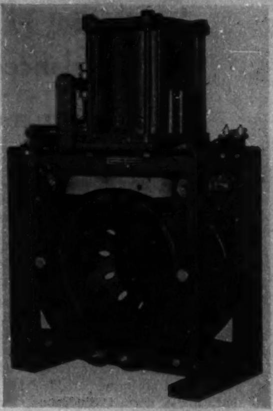


Figure 10. Magnetic brake used on unit cargo winch

It was necessary to run a series of tests to determine the manner in which the resistor should be built. The first test that was run on the resistors was to determine the amount of power that could be put in each box with different numbers of boxes carrying current. That is, the top box is capable of carrying 145 per cent of its open power rating when no other boxes are in the circuit, but when all seven boxes are in the circuit the top box can only carry 105 per cent of its open power rating without exceeding the permissible temperature rise. These data were used to determine the best location for each section of resistance in the stack of boxes. After the sections were located, it was possible to estimate the length of time each section could be left in circuit for the various master-switch positions. Data regarding the time required for resistor units of this type to reach temperature with different percentages of their rated power applied made such estimating possible. On the basis of preliminary layouts, a sample resistor was made and tested under a great variety of operating conditions which would be encountered in actual service. Data already collected from numerous shipboard tests made over a period of years served as a background for determining how long the resistor should be capable of operating on each point. Assumed duty cycles are of little value in estimating the sizes of the various resistor sections because of the abuse this type of control receives from unexperienced operators. The final design of resistor contains 35 units and has ample capacity for operation on average duty cycle for 15 minutes without forced-air ventilation. Another feature of the resistor compartment is the thermal

Figure 11. Front view of resistor compartment with door removed

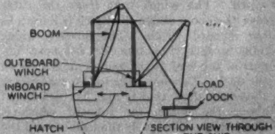


Figure 12. Sketch of burtoning method of loading and unloading a ship

switch which is employed to protect the resistor from dangerous overheating due to some unusual conditions, such as fan stoppage or blocking of the air-intake door. The fan motor is fed from the main contact tips on the control undervoltage relay and through the control fuses which should give fan operation as long as the control is energized.

The resistor is very difficult to protect, as it is essential to trip out the control for a small amount of power in the stack or from a relatively large amount of power in the same length of time. The reason for this is that the resistor is made up of six sections, used in various circuit locations, and each section has a different power rating and time on requirements. A number of different types of thermal switches was tested, and it was found that the switch had to have a best heat response to protect the edgewise resistor units which do not have a great amount of thermal capacity. The next problem was to determine the exact spot where it should be placed in order to protect as many of the sections as possible on the various operating parts. The location of the thermal switch was above the resistor stack, so that the heat representing large kilowatt dissipation low in the stack would trip the switch. The unit also would function with a relatively low kilowatt dissipation on the top units. The action in the latter case was mainly from radiation from the top units. This switch is entirely waterproof, as it is located where rain could damage it. It is not necessary to remove the thermal switch to take out resistor boxes, as it is mounted on the top of the air baffle. The thermal switch selected protected the resistor sections on most operating points as well as protecting the resistor on a normal operating cycle.

Appendix

The method of cargo handling known as burtoning consists of the use of two cables connected to the load. One cable is fed over a boom which is swung above the hatch of the vessel, while the other cable is fed over a boom above the dock. A separate cargo winch is used for each boom. Each of these winches is under the control of its own master switch. The winch that feeds cable over the boom above the dock is known as the outboard winch. The load is hoisted from the dock by the outboard winch. The winch that feeds the cable over the boom above the hatch is known as the inboard winch. The inboard winch pulls in slack cable but does not hoist any part of the load as the load is moved above the dock. As soon as the load is far enough above the dock that it may be transferred above the hatch, the inboard winch is speeded up. This pulls the load toward the hatch, and the load may be moved more by paying out cable from the outboard winch. The load may be lowered into the hatch by lowering the load on the inboard winch and paying out cable on the outboard winch at a speed slightly in excess of the inboard-winch cable. The sketch in Figure 12 indicates the relative positions of the cargo booms on the vessel. There is hardly any swinging of the load with experienced operators. This method of cargo handling permits rapid loading and unloading of a vessel.

Operation of Rectifiers Under Unbalanced Conditions

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THE development of single-anode rectifying elements has greatly stimulated the application of rectifiers to general industrial and essential duty loads. Sealed ignitron rectifiers[1] have been made available in a range of ratings, and industry is vitally interested in the performance of these equipments when operated under conditions of partial-anode complement, as when an anode is misfiring or is entirely out of service.

A multiphase mercury-arc rectifier and its associated transformer are designed, so that, under normal conditions, the current in all circuit elements will be balanced, and there will be no d-c components of flux in the transformer core. When the rectifier equipment is operated on less than normal-anode complement, the circuit is no longer symmetrical, and the remaining anodes do not share the d-c load currents equally. Under this condition, a d-c component of flux exists in the transformer core, and the interphase transformer is adversely affected by unbalanced currents in the two halves.

The purpose of this paper is to describe the distribution of currents in the various transformer windings and rectifying elements under conditions of unbalance and to indicate the effect of such unbalances on the circuit duty and the rectifier capacity.[2] The paper is concerned with the temperature rise of transformers, transformer life, and the duty on the rectifying elements arising from both the magnitude and wave shape of the anode currents. The problem is best approached from an analysis of measurements made on standard equipments.

Scope of Tests

The mercury-arc rectifier utilizes a number of rectifying elements in combination with transformers and other apparatus. The heating of most of the components is determined by rms values, and the equipment is designed to meet definite temperature rise limits. The important factor in establishing temperature limits is the thermal endurance of insulation and other materials used in construction. It is well known that the physical deterioration of insulating materials under the influence of time and temperature increases rapidly with temperature. The temperature rise, therefore, influences directly the probable life and reliability of the apparatus.[3]

The design and construction of the rectifying elements are such that time and temperature have little effect on life. The capacity of the device is limited only by the ability of the ionized vapor to conduct the current from the anode to the mercury pool and by the probability of arc-back which increases as the peak current increases. The usual design of the rectifying element is, therefore, a compromise between these factors. A design for low arc-back frequency is accomplished by baffles and shields which tend to aid the deionization properties at the expense of arc-drop and a decrease in the peak ampere capacity. These factors are not directly determined by temperature rise, since the capacity of the rectifying element is influenced by the characteristics of the circuit. The accelerated-load test, or "load-limit test,"[4] is used primarily to determine the capacity of the rectifying element.

In order to determine the d-c load current which can be carried when operating on less than normal-anode complement without undue heating of any part of the rectifier transformer, or exceeding the maximum loading at which the rectifying element is capable of operating without failure, it is necessary to know the distribution and wave shapes of current in the primary, secondary, and interphase windings. The procedure used in obtaining data of this nature is to make temperature measurements and to study wave shapes of currents with typical circuit arrangements, so that the analysis of

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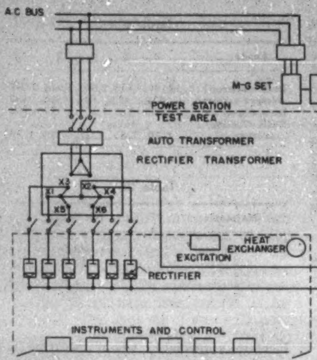


Figure 1 (left). Arrangement of test equipment

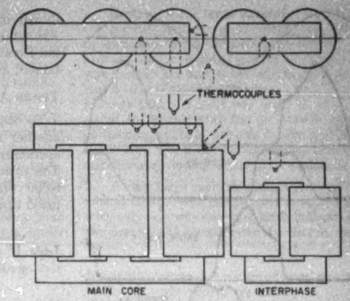


Figure 2 (right). Thermocouple locations

Double-wye transformer

data obtained in a few specific cases will enable the design engineer to predict the performance for a range of ratings.

A typical circuit diagram of equipment for determining rectifier-circuit duty operating with less than normal-anode complement is shown in Figure 1. Shunts and ammeters connected in the anode leads permit the reading of the average anode-current distribution. A multi-element oscillograph is used to obtain the wave shapes of all anode and transformer primary currents.

For most applications, circuits which utilize simple rectifiers operating in parallel are preferable. In such circuits the total load is divided equally between the simple rectifiers. Arc losses are a minimum, and the current capacity of the equipment is a maximum. Tests, therefore, were conducted on equipments with double-wye transformers rated 150 kw and 1,000 kw at 250 volts direct current. Tests also were made on a 200-kw 275-volt d-c double-biphase transformer. Test results on the 150-kw and 1,000-kw equipment were very similar; hence, discussion is limited to the 1,000-kw unit.

To determine the transformer heating, readings of the top oil, ambient, and core temperatures were taken under various conditions of loading. For core-temperature measurements, thermocouples were placed in the main and interphase core during assembly with leads brought out through a manhole opening in the cover. The location of the thermocouples for the double-wye transformer is shown in Figure 2. Because of the difficulty in locating the thermocouples on the hottest surface, which is the underside of the top yoke, they were placed between laminations near the top and measurements corrected for the hottest surface temperature.

To determine the duty on the rectifying elements, as well as to evaluate factors affecting the permissible transformer load, an analysis was made of the primary line current, anode currents, and voltage oscillograms.

Test Procedure

Normal Operation

To provide a basis of comparison, a normal heat run was made on a rectifier equipment with all anodes conducting. The rectifier transformer was rated 1,190 kva and 6,600/243 *L-N* volts with a double-wye secondary winding designed for a 1,000-kw 250-volt 6-anode ignitron rectifier. The primary voltage was maintained at 6,600 volts, 60 cycles, and the load was adjusted to a 4,000-ampere d-c output, at zero phase retard, and continued until constant oil temperature rise was obtained.

Since the a-c voltage across the interphase transformer and, consequently, its core losses increase with the amount of phase retard, the run was continued with 15 per cent phase retard, until constant interphase core rise over oil was reached. Oscillograms were taken of the primary

Figure 3. Core and top-oil temperature rise

1,000-kw double-wye transformer, five-anode operation

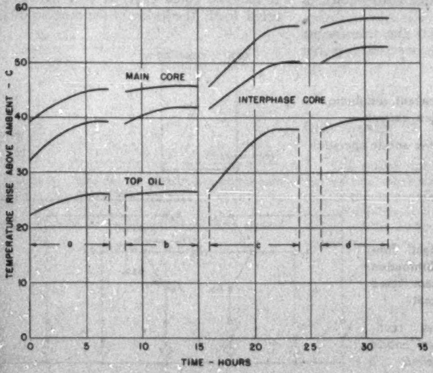
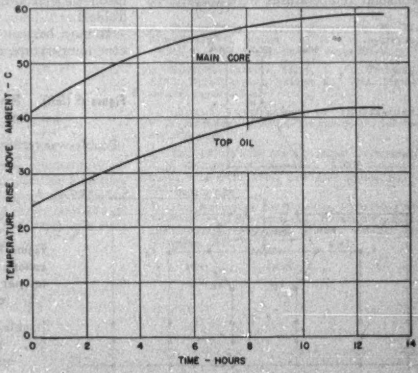


Figure 4. Core and top-oil temperature rise

1,000-kw double-wye transformer, three-anode operation



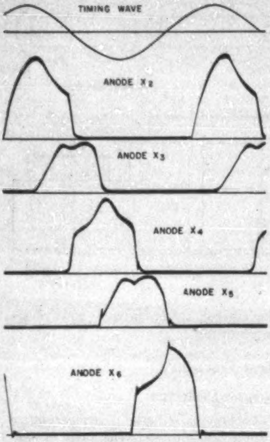


Figure 5. Anode-current wave forms, double-wye rectifier, anode X_1 inactive

voltage, primary and secondary currents in all phases.

FIVE-ANODE OPERATION

Temperature measurements were made on the rectifier transformer operating on five anodes with anode X_1 disconnected. The primary voltage was maintained at 6,600 volts, 60 cycles. The load was reduced to 2,300 amperes d-c output at zero phase retard and held until constant main and interphase core rise over oil was reached, as shown in Figure 3*a*. The phase retard then was adjusted to 15 per cent, and the run continued, as shown in Figure 3*b*.

The load was increased then to five sixths of full-load rating or 3,333 amperes at zero phase retard and maintained until constant temperatures were reached, as shown in Figure 3*c*. The phase retard was set at 15 per cent, and the load held until constant core rise over oil was obtained, as indicated in Figure 3*d*.

THREE-ANODE OPERATION

To obtain data with one wye inactive, another run was made with the rectifier operating on anodes X_2, X_4, and X_6. The primary voltage was maintained at 6,600 volts, 60 cycles. It was recognized that more than one-half load could be carried with one wye operating; therefore, two-thirds rated load or 2,670 amperes d-c output at 15 per cent phase retard was held until constant oil rise was obtained. The results of the test are shown in Figure 4 and Table I.

Table I. Temperature Rise—Degrees Centigrade at 15 Per Cent Phase Retard

Double-Wye Transformer

Per Cent Load	Number of Anodes	Top Oil Above Ambient Temperature	Main Core Above Oil	Interphase Core Above Oil
100	6	41.5	17.5	17
57.7	5	26.5	19.0	15
83.3	5	40.0	18.0	13
66.7	3	42.0	17.5	0

DOUBLE-BIPHASE RECTIFIER

Similar measurements were made on a 200-kw 275-volt double-biphase rectifier. A heat run was made operating with all anodes conducting at 15 per cent phase retard and 100 per cent load current. The test was continued when operating with three anodes at 15 per cent phase retard and 75 per cent load current.

Analysis of Data

TEMPERATURE MEASUREMENTS

A summary of temperature measurements of Figures 3 and 4 are shown in Table I.

It may be seen that the transformer core temperature rise over oil does not increase appreciably when operating a rectifier on less than normal-anode complement, and that, under any of these test conditions, the hottest core surface rise above ambient temperature did not reach the permissible limit of 65 degrees centigrade. The core temperature, therefore, is not a determining factor in limiting the load which can be carried under this condition of operation.

Table II

Anode	2	3	4	5	6
Peak amperes	3,159	1,810	2,610	1,948	3,087
Average amperes	831	427	644	526	832
Rms amperes	1,466	789	1,138	926	1,462
Ratio effective/ average	1.76	1.85	1.77	1.76	1.76

Table III

Harmonic	Per Cent I_L
Fundamental	89.0
2	45.0
3	
4	6.3
5	6.8

Table II shows the data obtained when an analysis is made on a typical oscillogram of anode current, as shown in Figure 5.

The rms current distribution in per cent of rated current in the various transformer windings of a double-wye rectifier with one anode inactive and with zero phase retard is shown in Figure 6. It may be noted that the output of the rectifier will have to be reduced approximately to 60 per cent of rated d-c current in order not to exceed the normal current rating of any winding. Under emergency conditions, however, it may be necessary to overload and, consequently, decrease the transformer life expectancy.[3]

The current distribution for a double-wye rectifier operating with three anodes in one wye inactive is shown in Figure 7. It may be noted that, at 66.7 per cent rated load, the primary current is 102

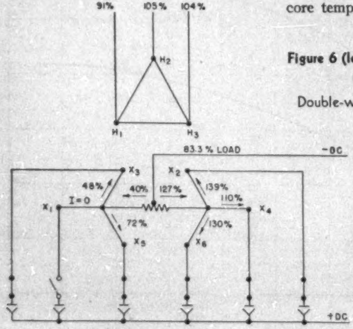


Figure 6 (left). Rms current distribution in per cent rated current

Double-wye rectifier, five-anode operation

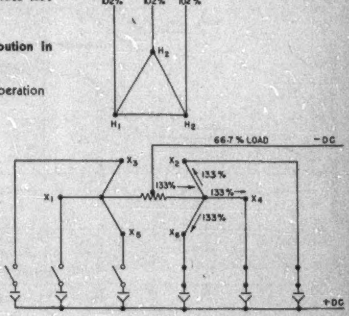


Figure 7 (right). Rms current distribution in per cent rated current

Double-wye rectifier, three-anode operation

Table IV. Temperature Rise—Degrees Centigrade

Per Cent Load	Number of Anodes	Oil Above Ambient Temperature	Main Core Above Oil	Teaser Core Above Oil	Interphase Core Above Oil
100	4	28	4	4	8
75	3	31	7	7	11

per cent of normal rated load which indicates a large component of exciting current and considerable d-c flux in the transformer core. Another disadvantage of operating with one wye is that the distortion of primary line current is increased. The analysis of an oscillogram of primary line current gives the harmonic components in per cent of the total effective current, as shown in Table III.

The temperature rises obtained when one anode is inactive in a double-biphase rectifier operating at 15 per cent retard are summarized in Table IV.

The rms current distribution in the various elements for 75 per cent rated load current is shown in Figure 8. The output will have to be reduced approximately to 50 per cent of full load in order not to exceed the rated current of any transformer winding.

Duty on Rectifying Elements

The duty on the rectifying elements when operating on less than normal-anode complement arises from both the magnitude and the shape of the current and voltage waves. Since the instantaneous arc-drop is a function of the instantaneous current, the peak current is an important factor in determining the duty on the rectifying element. On the other hand, the residual ionization is a function of the peak current which has passed during conduction and, therefore, influences the probability of arc-back. The ratio of peak current to load current in the anode following an inactive anode in a double-wye rectifier increases from 0.5 for normal operation to 1.0 under operation with one anode inactive. A similar increase is noted for the anode preceding an inactive anode.

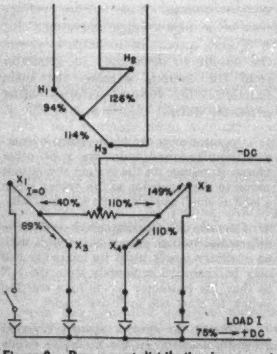


Figure 8. Rms current distribution in per cent rated current

Double-biphase rectifier, three-anode operation

The final commutation rate provides a further measure of the duty on the rectifying element. The residual ionization present at the beginning of the inverse period depends on the rate of reduction of current during the last few degrees of conduction. Kingdon and Lawton[5] have indicated that the frequency of arc-back is a function of the product of initial inverse voltage by the final commutation rate. Both of these factors are increased when one anode in a rectifier is inactive; however, if the loading is reduced to come within the current limitations of the transformer windings, the frequency of arc-back is not increased. Figure 9 shows load-limit tests on a double-wye rectifier operating with normal-anode complement compared with five-anode operation. The five-anode load limits are roughly 70 per cent of the limits with all anodes active.

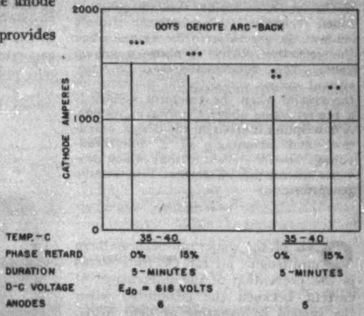


Figure 9. Load-limit tests

Double-wye rectifier, normal- and partial-anode complement

Conclusions

1. The transformer core temperature increases somewhat when operating a rectifier on less than normal-anode complement, but it is not a factor limiting the amount of load which can be carried under this operating condition.

2. Because of the large unbalances in rms coil currents with one anode inactive, it will be necessary to reduce the load to considerably less than five-sixths load for the double-wye connection and three-fourths load for the double-biphase connection. The tests indicate that the load will need to be reduced to 60 per cent for the double-wye connection and to 50 per cent for the double-biphase circuit, if the current rating of any transformer winding is not to be exceeded.

3. A very large exciting current is present when a double-wye transformer is operated with only one wye carrying load, indicating the presence of considerable d-c flux in the core.

4. If the anode of a double-wye rectifier is inactive, more load usually can be carried on the remaining five anodes than can be carried using one secondary wye.

5. If one anode of a double-biphase-connected rectifier is inactive, as much load can be carried on two anodes operating single biphase as can be carried on the three remaining anodes.

6. When the loading is reduced, because of the current limitations imposed by the transformer windings, the arc-back frequency is not increased when operating a double-wye rectifier on less than normal-anode complement.

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An Aircraft Differential-Voltage Cutout

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Synopsis: Experience obtained with present multiengine aircraft electrical systems indicates a need for preventing interchange of current between generators when the system is operating at light loads. Such current interchange causes reverse-current cutouts of conventional design to connect and disconnect their respective generators to the system so rapidly that the action is called "chatter." Conventional cutouts connect the generator to the system when the generator voltage is above a pre-set value. The differential-voltage reverse-current cutout connects the generator to the system when the generator voltage is a pre-set value above the system voltage. A description is given on the design, operation, and advantages of a differential-voltage reverse-current cutout which prevents interchange of current between the generators.

ONE of the major problems on large multiengine aircraft electric systems is the prevention of the interchange of current between the generators when the system is operating at light loads. Parallel operation of the generators is accomplished with a circuit using equalizer windings on the voltage regulators. The effectiveness of this circuit is proportional to the difference in load current of the generators. This means that there is no paralleling action at no load and very little paralleling action at light load, which is the normal condition on an airplane. Loads nearing the capacity of the system may be encountered for only a few minutes on each flight. Voltage regulators have an accuracy of plus or minus two per cent over the entire range of ambient temperatures and generator speeds. The nominal setting for voltage regulators is now 28 volts. The specified tolerance allows the voltage regulators to regulate the generator voltage at any point between 27.5 volts and 28.5 volts. This small variation in voltage causes reverse current to flow to one or more generators. Reverse current flowing to a generator causes the reverse-current cutout to disconnect the generator from the system. However, the generator voltage may be above the pickup setting of the cutout so that it will reconnect the generator to the system.

Consider the case of an aircraft electrical system containing two generators operating in parallel under the control of two voltage regulators and two conventional cutouts.[1,2] The voltage regulator of the first generator is set at 28.5 volts and its cutout is set to connect the generator to the system at 26.5 volts. The voltage regulator of the second generator is set at 27.5 volts and its cutout is also set to connect the generator to the system at 26.5 volts. With both generators connected to the system, current will flow from the first generator to the second generator. This condition causes the cutout of the second generator to disconnect it from the system. With the cutout of the second generator opened by reverse current, the voltage on its operating coil is 27.5 volts; which is one volt higher than its operating voltage. Therefore, the cutout of the second generator will reconnect the generator to the system and the action just explained repeats. Due to the rapidity of this action, it is called "chatter." When this condition occurs the respective cutouts are continually operating, shortening their life in some instances to relatively few hours.

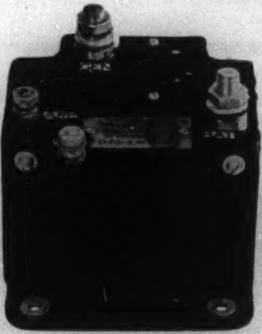

Figure 1. A 200-ampere differential-voltage cutout

From this description it is evident that there is a need for a method of preventing interchange of current between generators when the system is operating at light loads.

The simplest means of eliminating the cutout "chatter" is to change the principle of operation from a device which measures the generator voltage (automotive type) to a device which measures the difference between the generator voltage and the system voltage. Hence, this new cutout is called a differential-voltage cutout. The differential-voltage cutout operates when the generator voltage exceeds the system voltage by a predetermined difference voltage. The system voltage may vary over a wide range without "chatter" of the differential-voltage cutout. In electrical systems using the differential-voltage cutout a generator cannot be connected to the system until the system voltage decreases to a point where the generator has sufficient voltage to supply part of the load.

Description

Figure 1 shows a differential-voltage reverse-current cutout designed for use with any d-c generator having a normal regulated voltage of 28.5 volts and a current rating up to 200 amperes. The device occupies a four-inch cube and weighs two pounds six ounces. Four mounting holes on three-inch centers are provided in lugs extending from the cover. All terminals extend from the top so that cables may be attached from any direction. The unit is entirely enclosed for operation in atmospheres of the severest dust and moisture content. Its operation shows negligible change when vibrated at frequencies up to 55 cycles per second with a total excursion of $^{1}/_{16}$ inch.

The differential-voltage cutout connects the generator to the system when the generator voltage is 0.4 volt above system voltage. The system voltage may be at any voltage above 20 volts. A reverse current of 16 amperes causes the cutout to disconnect its generator from the system. Besides the main function, the cutout performs other necessary duties:

1. Operates on a dead bus—when a generator is up to speed and there is no other source of voltage on the system this cutout connects the generator to the system when a load as small as eight watts is connected.

2. Operates as a starting switch—when the differential-voltage cutout is installed with an auxiliary power plant its contactor coil may be energized separately from the bus so that the contactor serves as a starting switch to start the auxiliary engine.

3. Protects the system against reversed generator polarity—if the generator builds up with reversed polarity, it is possible to have 60 volts across the differential circuit of the cutout. Under this condition the cutout does not operate nor is the differential circuit damaged even though its operating voltage is only 0.67 per cent of this value.

4. Protects the system against short circuits—should a generator suddenly lose its field or become short-circuited, a heavy surge of reverse current will flow through it.[3] The cutout must be capable of opening

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this current without damage to the cutout or further damage to the generator. The contactor of this cutout has interrupted 3,500 amperes at 50,000 feet altitude.

Design

A differential-voltage cutout presents several design problems not encountered with the conventional cutout. The primary considerations are protecting the differential-voltage winding against overheating and interrupting the differential-voltage winding to prevent discharging the battery when the generator is idle.

Because the differential-voltage winding must have sufficient excitation to operate on an extremely low voltage, it is impractical to make it withstand full system voltage (for the case of a dead generator) or double system voltage (for the case of a reversed generator) without overheating. The winding is protected by connecting a relay across the differential circuit which operates at a low enough voltage to protect the differential-voltage winding by inserting a resistor in the circuit.

On the conventional cutout the operating coil is connected to the generator side of the contactor so that the coils are not energized when the generator is idle. The nature of the differential-voltage cutout makes it necessary to interrupt the differential-voltage circuit, in addition to the main circuit, to prevent discharging the battery when the generator is idle.

Two main units comprise the cutout illustrated by Figures 1 and 2: a polarized differential-voltage reverse-current relay, distinguished by the square coil, and a magnetic contactor. Two auxiliary relays control the differential-voltage winding on the reverse-current relay. Figure 3 is a schematic diagram of the cutout.

The square spool of the reverse-current relay coil contains three windings. An inner differential-voltage winding *RCD* is connected between the generator positive terminal and the battery positive terminal through the contacts of the two auxiliary relays. A holding winding *RCH*, in series with a 470-ohm resistor, is connected between the reverse-current relay contacts and ground. An outer series winding *RCS*, consisting of a single turn of copper strap, is connected between the generator positive terminal and the generator side of the contactor. The differential-voltage winding operates the reverse-current relay contacts *RC*. The holding winding produces the magnetic force to hold the reverse-current relay contacts *RC* closed when the differential-voltage winding is short-circuited by the contactor contacts *C*. The series winding adds magnetically to the differential-voltage and holding windings when the generator is supplying current to the system. Polarization is accomplished with a permanent magnet. The design of this relay produces a strong opening force on the relay armature when a heavy surge of reverse current, such as can be caused by a short-circuited generator or the loss of a generator field, flows through the series winding. As the reverse current increases, the relay opening force increases.[1] It is impossible for this type of relay to "lock in" on a heavy reverse current.

On other types of reverse-current relays it is possible for a heavy surge of reverse current in the series winding to reverse completely the polarity of the magnetic circuit. When this condition occurs, the relay holds its contacts closed and the heavy reverse current continues to flow, causing additional damage to the system.

A special design is used on the operating magnet of the contactor. Magnetically it operates as a solenoid magnet in the open position and as a lifting magnet in the closed position. This arrangement uses advantages of each type of magnet and produces a contactor of minimum weight for the forces obtained. The contactor coil is composed of a main winding *CM*, and a holding winding *CH*. The main winding has low resistance and is used to operate the contactor. When the reverse-current relay contacts *RC* close, full generator voltage is impressed across this winding. As the contactor contacts *C* close an electrical interlock opens, making the holding winding *CH* in series with the main winding. The contact force obtained with this contactor is 4.5 pounds per contact, producing a voltage drop across the cutout of only 60 millivolts at 200 amperes.

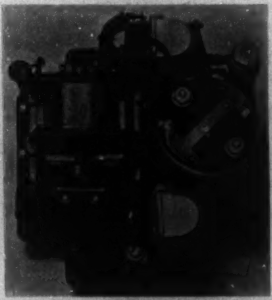

Figure 2. A 200-ampere differential-voltage cutout with cover removed

Both auxiliary relays have two contacts in parallel for added reliability in completing the low differential-voltage circuit. Relay *A*1 is normally open and is in series with a rectifier. It is used to complete the differential-voltage circuit when the generator voltage is over 20 volts. The rectifier is used as a d-c valve and prevents the closing of relay *A*1, if the generator builds up with reversed polarity. Relay *A*2 is normally closed and is connected across the differential-voltage winding. It protects the differential-voltage winding against over voltage. When the voltage on the differential-voltage winding exceeds four volts a resistance of 80 ohms is inserted in the circuit.

Operation

With switch *S* in the number 1 position the differential-voltage cutout is ready for operation. When the generator speed is increased until its terminal voltage is 20 volts, relay *A*1 will operate and complete the differential-voltage circuit. With a battery voltage of 25 volts there is 5 volts across the differential winding. Since this voltage is also applied to the coil of *A*2, the latter operates and inserts the resistor in series with the differential-voltage winding *RCD*. Increasing the generator voltage decreases the differential voltage. When the generator voltage is equal to the battery voltage there is no voltage on the coil of relay *A*2. Therefore, *A*2 releases and closes its contacts, causing the resistor to be short-circuited. Now the circuit has only the low resistance differential-voltage winding to limit the current in the differential-voltage circuit. Increasing the generator voltage to 0.4 volt above the battery voltage causes contacts *RC* to close, energizing the contactor coil *CM* and the reverse-current relay coil *RCH*. The contactor coil closes contacts *C* which connects the generator to the load bus. The differential-voltage cutout is now in the normal operating position. Decreasing the generator voltage below the battery voltage causes reverse current to flow from the battery to the generator. With current flowing in the reverse direction, relay coil *RCS* magnetically opposes relay coil *RCH*. Increasing the reverse current to 16 amperes causes the reverse-current relay to open the contactor coil circuit and disconnect the generator from the load bus.

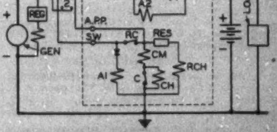

Figure 3. Schematic diagram of the differential-voltage cutout

If the generator builds up with reversed polarity the rectifier will block the current and prevent the closing of relay *A*1.

To use the contactor of the differential-voltage cutout as a starting switch for starting an auxiliary engine, place switch *S* in the number 2 position during the starting period.

Advantages

Two fundamental improvements are obtained in the operation of aircraft electrical systems by using the differential-voltage cutout in place of the conventional cutout:

1. Cutout "chatter," which occurs with conventional cutouts when the system is operating at light loads, is eliminated.
2. The generator may be connected to the system at any voltage above 20 volts. This is particularly important on long flights where the generator speed is below the speed necessary to produce normal voltage. With this condition the battery supplies all of the load current until its voltage drops below the generator voltage. At this point the generator is connected to the system, preventing complete discharge of the battery.

Three additional improvements are included in the differential-voltage cutout which produces greater reliability of operation than is obtained with the conventional cutout:

1. The contactor of the cutout protects the system when a short circuit occurs in the generator.
2. Operation is not impaired in atmospheres of the severest dust and moisture content.
3. Operation is not impaired by vibration conditions encountered in aircraft service.

Conclusions

It has been shown that a cutout which is designed for differential-voltage operation has all of the advantages and none of the disadvantages of the automotive type of cutout. Also, the differential-voltage cutout has several advantages not obtained by using any other principle of operation.

The outstanding success of the differential-voltage cutout is exemplified by the preparation of an Army-Navy Specification for a differential-voltage cutout intended to supersede the automotive type of cutout.

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A Unique Moving-Magnet Ratio Instrument

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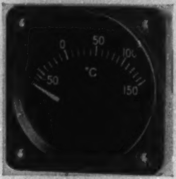
Synopsis: This paper describes a unique type of moving-magnet d-c ratio instrument. An instrument designed for temperature measurement in aircraft is described in detail.

THE remote indication of oil and coolant temperatures on aircraft has been accomplished principally through the use of temperature-sensitive resistance bulbs and moving-coil ratio instruments. A survey of the requirements indicated that a moving-magnet instrument could be developed which would fulfill these requirements and have characteristics of simplicity, serviceability, ruggedness, and lightness. Such instruments, both single and dual, are illustrated in Figure 1.

Principle of Operation

In this instrument the permanent-magnet rotor position and the indication are determined by the fluxes produced by two mutually perpendicular coil groups. One coil group carries substantially constant current; the other carries current which is a function of the temperature bulb resistance.



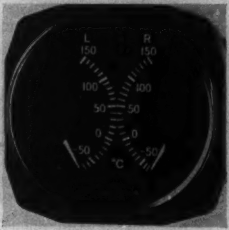


Figure 1. Moving-magnet ratio-type temperature indicators

Figure 2 shows the coils and rotor arrangement. The small outer coils serve to set up a diametrical flux along the axis of these coils. This flux corresponds to vector *B* in Figure 3. The current in these coils is approximately constant for all indications at any given supply voltage.

The large inner coils serve to set up a diametrical flux along the axis of these coils and perpendicular to the axis of the small outer coils. This flux corresponds to vector *A* in Figure 3. The current in these coils is a function of the quantity to be indicated. These coils serve as the detector in a Wheatstone bridge in which case the coil current is zero near center scale, a positive maximum at one end of the scale, and a negative maximum at the other. This is illustrated in Figure 3 where vector *A* is downward at −70-degrees-centigrade indication, zero at +40-degrees-centigrade indication, and upward at +150-degrees-centigrade indication. In this diagram vector *C* is the resultant flux. The rotor is magnetized parallel to the flat surfaces, and the rotor aligns itself with the resultant flux lines. Figure 3 is helpful for purposes of an elementary explanation of operation but does not include various distorting effects.

In the actual device the two sets of coils are surrounded by a high-permeability cylindrical shield. The purpose of this shield is to provide a return path for the internal fluxes and to screen out extraneous fields.

The Circuit

The circuit used is shown in Figure 4. Resistors R_1, R_2, and R_3 are fixed arms in a Wheatstone bridge, and R_4 is the resistance-type thermometer bulb. All resistance values are practically independent of temperature except those of the coils and bulb. Bulb-resistance characteristics are shown in Figure 5. The bridge circuit is balanced by the bulb at approximately center-scale (+40 degrees centigrade) indication and is unbalanced at all other indications. At indications below approximately 40 degrees centigrade unbalance current flows through coil *A* from R_2 to R_4. At indications above this value current flows through coil *A* in the opposite direction. Because of the values of fixed resistances in the circuit, variations in bulb

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resistance cause less than ±5 per cent variation in the outer-coil current for all scale indications at any given values of voltage and ambient temperature. For elementary considerations this current may be assumed constant.

Construction and Adjustments

Figure 6 illustrates the construction of a single-element temperature indicator. The element is cut away showing the close magnetic coupling between the rotor and the two inner coils. These coils are wound on insulated copper-coil forms, which provide a damping structure close to the rotor to damp excessive pointer oscillations.

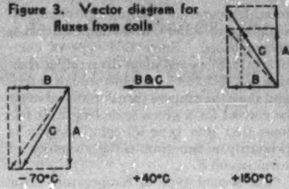
The permanent magnet rotor material is sintered oxide, chosen for its high coercive force and low density. The dimensions of the rotor were selected as a result of tests made to determine the optimum allocation of space to coils and rotor, for the case of a 0.7-inch-diameter enclosing shield. The rotor is mounted on an aluminum shaft provided with tantalum–tungsten pivots. The pointer is secured to the shaft over locating flats by a nut.

Directly above the element are two adjustable resistor spools, each consisting of a fixed-coil portion and a small rheostat.

Resistance R_1 in Figure 4 includes one of these adjustable rheostats. This controls the balance point of the bridge and, consequently, substantially fixes the location of the center-scale point.

Resistance R_7 is similarly adjustable by the other rheostat and by shunting action determines the amount of current in coil *B*, hence, the length of vector *B* in Figure 3, and consequently the scale length. Permanency of these adjustments is accomplished by soldering the rheostat contact arms to the windings.

Figure 3. Vector diagram for fluxes from coils



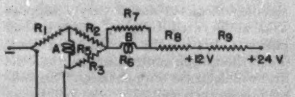


Figure 4. Temperature-indicator circuit

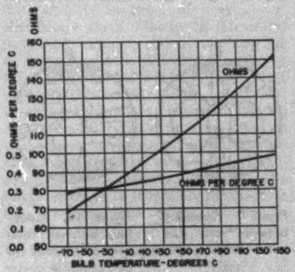


Figure 5. Temperature–bulb-resistance characteristics

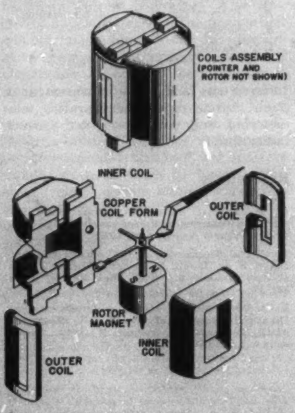


Figure 2. Arrangement of coils and rotor

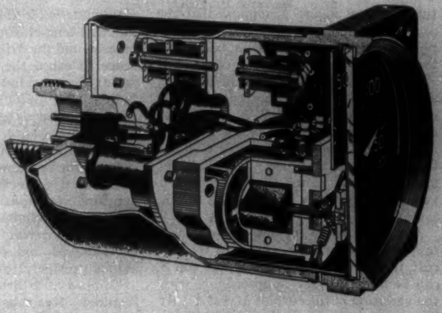


Figure 6. Sectional view of single-element moving-magnet ratio-type temperature indicator

Scale Distribution

In order to attain the required linear scale the introduction of nonlinear effects is necessary. Figure 7 shows the crowding at the scale ends, which would occur if the effective vector *B* of the small outer coils were constant for every scale position, and if the vector *A* of the inner coils were of a magnitude proportional to the variation of the indicated quantity from the center-scale value. Figure 8 shows how a gradual shortening of vector *B*, with departure from center scale, makes the scale distribution uniform.

The nonuniform distribution of Figure 7 is approached when a purely cylindrical rotor is used. The uniform distribution of Figure 8 is practically realized if the rotor has flat sides parallel to the rotor-flux lines.

Figures 9 and 10 illustrate the manner in which the flat-sided rotor effects a linear-scale distribution.

The flat-sided rotor produces a large reduction in magnetic coupling between the outer coils and the rotor with rotation away from the center-scale position. This is illustrated by Figures 9 and 10. This effect corresponds to the desired shortening of vector *B*.

Power-Failure Indication

Power-failure indication is accomplished by a small magnet assembled in the subbridge with the magnetic axis perpendicular to the center-scale rotor axis, so that, in the absence of excitation, the pointer is moved off the lower-scale end. The torque of this small magnet is supplemented by a permeance torque obtained by shaping elliptically the shield enclosing the coil structure, and by aligning the minor axis properly.

Both torques vary in an essentially sinusoidal manner with the permeance torque in second harmonic relationship to that of the small magnet. These torques and their uniform resultant are shown in Figure 11.

The uniform resultant provides adequate torque well beyond the scale ends and eliminates the peak at center scale, thus minimizing the frictional and voltage-error problems.

The down-scale shift in indication, which these torques tend to produce, is corrected by adjustment of R_3, Figure 4.

Voltage Error

Fundamentally, the rotor position depends upon the ratio of the currents in the two sets of coils and not upon magnitudes. In the absence of pull-off magnet and shield torques, the indication would be independent of applied voltage.

The pull-off magnet and shield torques cause the pointer to move down scale as the supply voltage and. consequently, the torque contributed by the coils are reduced. Reduction of voltage from 28.5 to 22.5 volts generally causes errors less than three degrees centigrade at any scale point.

Ambient-Temperature Compensation

Errors caused by change in ambient temperature are of two types. The first is a change in scale length which is caused by disproportionate changes in the currents in coils *A* and *B*. With reference to the right-hand diagram in Figure 3 and the circuit of Figure 4, suppose that the ambient temperature is raised, and suppose that R_7 is omitted. Then, because of the increased resistance of the copper coil *A*, the current in that coil would decrease, and vector *A* would be shortened. However, the current in coil *B* would not change appreciably, and vector *B* would not change. The dashed vector *C* shows the resultant in this case. The angle between the solid vector *C* and the dashed vector *C* represents a shortening of the scale.

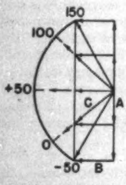


Figure 7. Scale distribution with cylindrical rotor

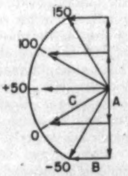


Figure 8. Scale distribution with flatted rotor

If resistance R_7 of low-temperature-resistance coefficient is placed in the circuit, then the current in the copper coil *B* can be made to vary in accordance with the variation of the current in coil *A*. At room temperature a portion of the battery current passes through coil *B* and is governed by R_6 and R_7. At elevated temperatures a lesser portion of the battery current passes through coil *B*. With reference to Figure 3 again it is apparent that, if vectors *A* and *B* are shortened proportionately, the resultant vector falls on the room-temperature resultant, no change in scale length occurs, and compensation is accomplished. Since this type of compensation depends upon diverting a fraction of the battery current through coil *B*, and since the ampere turns produced must be correct for a given scale length, it follows that this type of compensation is primarily a function of the number of turns in coil *B*.

The second type of temperature error is caused by a change in torque as the resistance of coils *A* and *B* vary. Suppose that at elevated temperature both coil currents decrease by say ten per cent. Then the torque contributed by the coils also will decrease ten per cent. The pull-off torque then will cause the pointer to move in a down-scale direction for all indications. This effect is compensated by including in bridge arm R_2 a certain amount of positive-temperature-coefficient material, in this case, copper. If the ambient temperature is elevated R_2 increases, and the bridge values change in a sense to cause the pointer to move up scale. Since bridge arm R_2 is opposite to bulb arm R_4, an increase in R_2 acts in the same direction as an increase in bulb resistance.

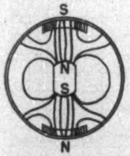


Figure 9. Coupling between rotor and outer coils at center scale

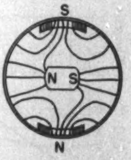


Figure 10. Coupling between rotor and outer coils 90 degrees from center scale

Self-heating errors are negligible because of the very low heat dissipation and the corrective effects of the ambient-temperature compensations.

Table I includes observed ambient-temperature errors on a typical instrument at −50- and +75-degree-centigrade ambient temperatures.

Compensation attainable is better than required. It is thus possible to use R_7 for both compensating and scale-length-adjusting purposes with satisfactory results.

Factor of Merit

Table ,I. Observed Ambient-Temperature Errors

Indication—Deg C	Observed Temperature Error—Deg C	
	At −50	At +75
− 70	0.0	−1.0
− 30	−0.5	−0.5
0	0.0	−0.5
+ 30	0.0	−0.5
+ 80	−0.5	+1.0
+120	+1.0	+1.0
+150	−1.0	+1.0

On an equivalent D'Arsonval basis the torque-to-weight ratio of the temperature indicator averages unity with variations of ±20 per cent.

Conclusions

The instrument described has the properties of lightweight, ruggedness, simplicity, and serviceability required for aircraft use. In addition the factor of merit is good, the voltage errors are low, self-heating errors are negligible, and ambient-temperature errors are practically compensated.

The instrument may be adapted to many applications where the characteristics of a ratio instrument are desirable.

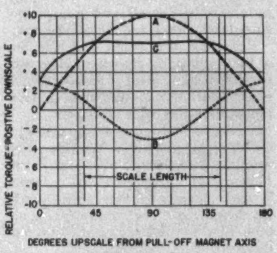


Figure 11

A. Torque curves of pull-off magnet
B. Shield
C. Resultant

Inferred uses include the measurement of ohms, current ratios, voltage ratios, telemetering, and voltage-weighted current indications.

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Protection of Large D-C Machines by Means of High-Speed Circuit Breakers

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THE problems of protecting large installations of rotating d-c machines have been neglected to some extent by the lack of suitable high-speed switchgear rugged enough to stand the severe duty usually encountered. The authors are describing their analysis of probable short-circuit characteristics of such large systems and a high-speed breaker rated at 7,500 amperes which has been developed for this particular purpose. The authors are also describing a large rotary-converter installation built some time ago which has recently been modernized by means of modern switchgear in order to overcome the serious damage caused to the rotating machines resulting from the inadequacy of the originally installed switchgear.

Comparison of Short-Circuit Conditions (A-C and D-C)

There has been some confusion in regard to application of ultrahigh-speed breakers. Perhaps the curves shown in Figure 1 can help to clear up this confusion.

Curve 1 is a fully offset 60-cycle current wave. The current dies down after a few cycles and becomes symmetrical, and an attempt to interrupt it at the first one-quarter-cycle peak would merely impose a severe duty on the breaker contacts as they would operate when the maximum current is flowing. The peak electromagnetic forces in the installation will not be cut down by the one-quarter-cycle opening of the breaker. From the standpoint of breaker performance it would be desirable to delay its opening as it then will have to interrupt lower currents. A compromise must be made between system stability and breaker performance and standard a-c breakers usually open in three to five or even eight cycles. Unless system stability is of prime importance there is no material gain in paying the higher cost of high-speed breakers.

Curve 2 is a d-c curve illustrating the condition existing in a system where the d-c generators are located an appreciable distance apart. The rate of rise is very low due to the inductance, and the ultimate current is low due to the ohmic resistance of the system. Except for preventing damage caused by commutator flashovers no gain is made by the use of high-speed breakers and commercial breakers with opening speeds of $1^3/_4$ to three cycles can therefore satisfactorily be employed.

Curve 3 shows the d-c rate of rise of a 300-volt 3,000-kw generator under short-circuit conditions. The initial rate of rise is 4,500,000 amperes per second and the ultimate current is 450,000 amperes. A three-cycle breaker would have to interrupt 200,000 amperes and a $1^3/_4$-cycle breaker would have to interrupt 125,000 amperes. A $^3/_8$-cycle breaker (time to peak) would have to interrupt only 23,000 amperes. Here the value of the high-speed breaker is clearly indicated. The electromechanical stresses in the machine and the connecting bus-bar structure or conductor is only 0.77 per cent when using a high-speed breaker. The various manufacturers of d-c machines do not seem to agree on the initial rate of rise and the ultimate current on a short-circuit condition, and a discussion of this problem would be of interest, but from test data which we have on a 650-volt 6,000-kw machine we have established beyond a doubt that the initial rate of rise is more than 6,000,000 amperes per second. This curve is shown as curve 4.

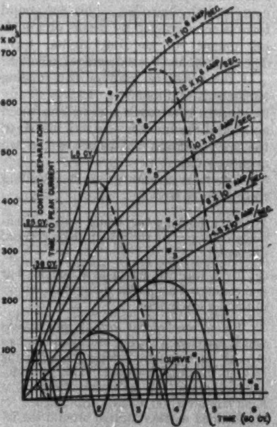

Figure 1. Short-circuit curves comparing effect of opening speed of circuit breakers

As more machines are added to the system, curves 5, 6, and 7 probably apply. If we use a three-cycle breaker in a d-c system represented by curve 7 we will have to interrupt the fantastic current of 700,000 amperes while a 0.38-cycle breaker (time to peak current) would have to interrupt only 80,000 amperes. In this case the stresses are again only 0.77 per cent if a high-speed breaker is used.

Assume a bus of 100-foot length used in this latter system with the busses placed two feet apart and consisting of two six-inch-square tubes, the force per running foot will be 110,000 pounds, requiring insulators spaced 0.064 feet apart if a three-cycle breaker is used. This of course is impractical. If we use a 0.38-cycle breaker (time to peak circuit) the force is only 1,450 pounds per running foot and the insulator spacing of 4.8 feet can be safely employed. This latter scheme would appear to be more reasonable.

It is appreciated that a bus-to-bus fault in a modern installation is rather uncommon but they do occur with disastrous results and it is emphasized that sound engineering practice dictates the installation of modern high-speed switchgear as the one solution to the problems which are encountered by high current and the electromagnetic forces they set up in the system.

Oscillographic Comparison of Current Against Time

Figure 2 shows oscillogram of an *FB* breaker interrupting a short circuit on two 3,000-kw generators at 550 volts, in parallel. The mechanical time of the breaker is 0.212 cycle, time to peak is 0.403 cycle, total time 82 cycles except for the small amount of trailing current which is permitted to flow through the arc interrupter resistors in order to keep the breaker voltage within reasonable limits. The maximum current is 26,600 amperes and the initial rate of rise is 4,920,000 amperes per second. The breaker was tripped by the reverse current feature, which trips the breaker in the shortest time possible.

The oscillogram in Figure 3 shows the breaker interrupting the same circuit but tripped by the forward-current overload feature. Due to the inherent characteristics of the overcurrent feature which employs an open air gap with a moving armature, the breaker now trips somewhat slower. The mechanical time of the breaker is 0.5 cycle, time to peak current is 0.667 cycle, and the total time is 1.17 cycles. The maximum current is 36,200 amperes or 9,600 amperes more than the current is in Figure 2 because

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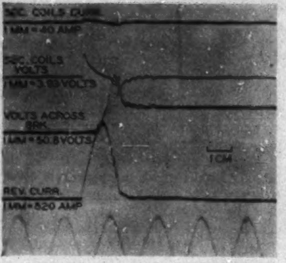

Figure 2. Oscillogram of *FB* breaker interrupting 550-volt d-c reverse-current trip

the mechanical time of the breaker is 0.288 cycle slower.

Figure 4 shows the same breaker opening the same circuit but at 650 volts and tripped by the forward current trip. The mechanical time is again 0.5 cycle, peak 0.645 cycle, total time 1.16 cycles, and maximum current 47,000 amperes, an increase of 8,700 amperes by increasing the generator voltage by 100 volts over Figure 3.

Description of Type-FB Breaker

A schematic cross section of the *FB* breaker is shown in Figure 5. The current path through the breaker is from the upper terminal through the magnetic blowout coil to the upper contact. From here it flows through the bridges to the lower contact where the circuit divides into two parallel circuits which then meet in the lower terminal.

There are two blowout coils, one located on each side of the arc chute and connected in parallel. Each coil consists of one turn and the coils are physically located so that the core of the coil is in line with the region in which the arc is originated. The return path for the blowout flux is through a laminated-iron circuit. The laminations extend through the core of the blowout coil to the outside face of the arc chute so there is no time delay in building up the blowout flux due to eddy current. Two slotted pole faces extend from the extremity of the laminated core to distribute the flux for proper arc interruption.

The movable bridge contacts are supported in an extremely light carrier made of heat-treated aluminum and the movable arcing contacts are in turn mounted on this carrier. The necessary contact pressure between the stationary and moving contacts is furnished by a completely independent separate spring on each contact located between the contact and the contact carrier.

The carrier is biased toward the open position by means of two opening springs, one located inside the other. The stronger of the two opening springs has a limited stroke and acts on the contact carrier only during the initial acceleration period. The weaker of the two springs is active during the whole opening stroke and definitely holds the contact carrier in its open position. The kinetic energy stored up in the moving contact during the opening stroke is absorbed at the end of the stroke by means of a friction-type buffer, preventing rebound of the contacts.

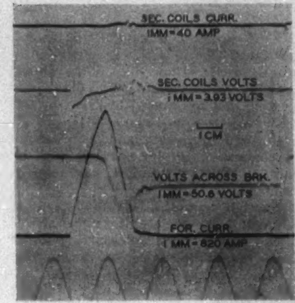

Figure 3. Oscillogram of *FB* breaker interrupting 550-volt d-c forward-current trip

The contact carrier is held in the closed position by means of a clutch shown in the released position on Figure 6. This clutch consists of two hardened steel jaws. The two jaws contact a hardened steel roller on opposite sides. The opening force of the breaker which is 5,000 pounds tends to force the two jaws apart with a force of 300 pounds, due to their angle of contact with the roller. This 300-pound force is counteracted by means of a holding magnet of the bucking-bar type. The magnet has been described in detail.[1]

If the magnetic pull of the holding magnet is temporarily lost for any reason, the two clutch jaws will be forced apart and the breaker contacts moved to the open position by means of the two opening springs.

There are several ways the holding force may be lost:

1. By a bucking bar which will momentarily demagnetize the armature on reversal of current.
2. By a trip coil acting as a bucking bar and connected across the interpole windings of the d-c machines.[2]
3. By a mechanical prying mechanism which exercises a prying force greater than that of the magnetic holding force tending to keep the breaker closed. This mechanical mechanism can be operated by (*a*) a hand trip lever, (*b*) a shunt trip magnet, (*c*) an overload magnet located around the inductive leg of the trip circuit, or (*d*) an overload magnet located around the lower terminal.

As already explained the circuit from the lower contact to the lower terminal is divided in two paths. The upper path passes through the holding magnet and the lower path is surrounded by a stack of iron laminations. With a steady current flowing through the breaker the current will divide between the two paths proportional to the conductances of the two paths. This is true whether the current flows in a forward or reverse direction. With a rapidly changing current flowing through the breaker, the current will divide in the two paths inversely proportional to the inductances of the two paths. This is also true whether the current is flowing forward or in a reverse direction.

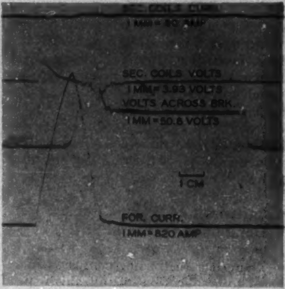

Figure 4. Oscillogram of *FB* breaker interrupting 650-volt d-c forward-current trip

On a quickly rising reverse current, the portion of the current which flows through the holding-magnet circuit will cause the holding flux through the armature to be diverted to a magnetic bypass. The armature will momentarily lose its pull and the breaker will trip for this reason. On slowly rising reverse current

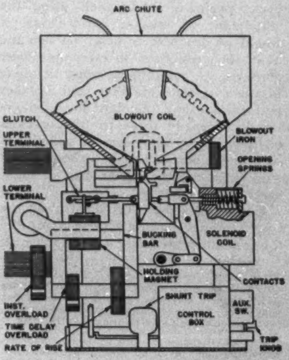

Figure 5. Section of type-*FB* breaker

the same effect will be obtained but at a higher current value.

If we place an overload magnet on the inductive part of the circuit, the overload magnet can be arranged to trip the breaker at a maintained current of sufficient value. A quickly rising current will pass harmlessly through the upper portion of the circuit due to the inductance of the lower circuit. This effect will afford overload protection of the machine and will permit harmless surges to pass through the breaker without tripping it. In other words, a time-delay overcurrent protection is provided without any mechanical timing means.

An overcurrent feature placed on the lower terminal leads where all the current which flows through the breaker will pass through this feature can be arranged to trip the breaker on excessively high current thereby tripping the breaker if its associated machine should contribute a high value of current into a fault on the system.

The operating mechanism consists of a solenoid which is arranged to close the breaker through an overcenter toggle mechanism. The opening springs and the contact buffer are built as an integral part of the closing mechanism. Therefore, all mechanical stresses in the breaker (except the contact pressures) are located in one common assembly and are not transmitted through any insulating material.

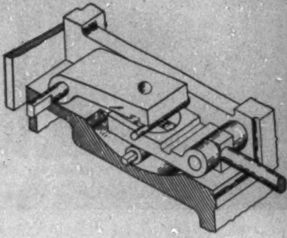

Figure 6. *FB* breaker clutch

Released position

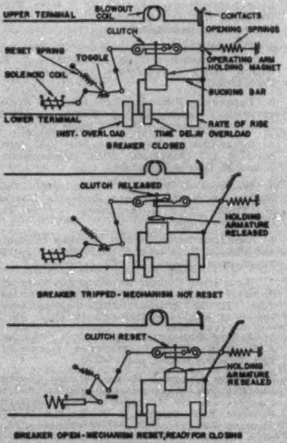
UPPER TERMINAL
BLOWOUT COIL
CONTACTS
OPENING SPRINGS
CLUTCH
RESET SPRING
TOGGLE
OPERATING ARM
HOLDING MAGNET
SOLENOID COIL
BUCKING BAR
LOWER TERMINAL
RATE OF RISE
INST. OVERLOAD
TIME DELAY OVERLOAD
BREAKER CLOSED
CLUTCH RELEASED
HOLDING ARMATURE RELEASED
BREAKER TRIPPED-MECHANISM NOT RESET
CLUTCH RESET
HOLDING ARMATURE RESEALED
BREAKER OPEN-MECHANISM RESET, READY FOR CLOSING

Figure 7. Schematic diagram of *FB* operating mechanism

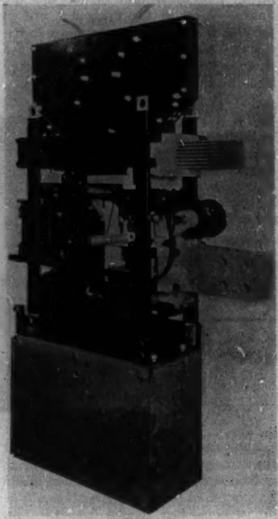

Figure 8. Right-side view of *FB* breaker

The various operating positions of the breaker are shown diagrammatically in Figure 7.

The top illustration shows the breaker closed. The opening forces in the breaker are here counteracted by means of the operating toggle which is locked over center.

The center illustration shows the breaker in the tripped position. The clutch is shown in its released position and the contact has been moved to the open position by means of the opening springs. The solenoid mechanism has not yet reset.

The bottom illustration shows the mechanism reset and ready for closing. This resetting was accomplished by means of the resetting spring causing the toggles to fold up thereby resetting the solenoid to its open position which in turn has permitted the two clutch jaws to engage the clutch roller.

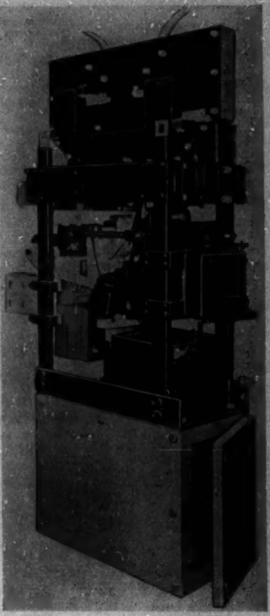

Figure 9. Left-side view of *FB* breaker

The arrangement provides trip-free operation at any part of the closing stroke. Hand operation is provided by means of a removable handle which can be inserted in a socket on one of the toggle links.

Two partial side views of the breaker are shown in Figures 8 and 9.

Description of Rotary-Converter Station

It will be of interest to describe a large d-c installation where the *FB* breakers are being installed to provide better protection of the rotating machines than was originally furnished for this installation. This plant has an installation of a large number of 2,500-kw 60-cycle rotary converters housed in two conversion stations. Only one station is being modernized and this station is described in detail.

The rotary stations are fed by a large hydroelectric power system with interconnections to other power systems. Fault capacities are very high in the a-c system, 1,500,000 kva on the 13.2-kv bus.

The station has double 13.2-kv a-c busses. The double-bus arrangement permits feeding all of the stations from one bus or splitting the load between the two busses. A one-line diagram of the

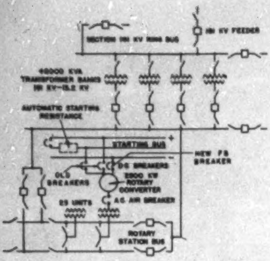

Figure 10. One-line diagram of rotary-converter station and bus

busses and station is shown in Figure 10. Double disconnecting switches are provided for each unit, permitting the units to be connected to either of the busses. These disconnecting switches will break transformer magnetizing current. The transformers were originally connected to the busses through oil circuit breakers. These have been removed and replaced with low-voltage air circuit breakers of 150,000 amperes interrupting capacity between the transformer secondary and the rotary converter.

Each unit has a positive, negative, and starting air circuit breaker on the d-c terminals as shown in Figure 13. These breakers were originally conventional air circuit breakers, opening in approximately eight cycles. The rotary converters are started from the d-c side by reduced-voltage starting. One unit is provided with an a-c motor for starting in case of complete station outage and there is a similar equipment in the other station to provide alternate means of starting. Several machines, and at times all machines, are operated in parallel on the d-c busses. The physical arrangement of the station is shown in Figure 11. Figure 12 shows the interior of the station.

These units were considered very large when installed in 1914 and were quite sensitive to disturbances, particularly those involving rapid changes in frequency. During such disturbances the units tend to pull out of step with each other and flash over. Such troubles often result in damage to the unit, particularly the main and equalizer busses, brush rigging, and commutators. There is also occasional damage to the armature winding. Flashover may also be started by trouble on an individual unit or on the d-c busses. Most of the flashovers are on the d-c side, although there are occasionally a-c flashovers. Most of the damage during flashovers is from energy from the d-c bus.

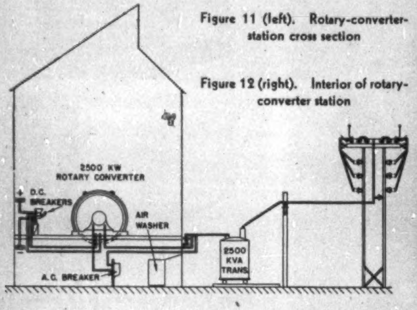

Figure 11 (left). Rotary-converter-station cross section

Figure 12 (right). Interior of rotary-converter station

Analysis of the troubles indicates that the damage to units and the transmission of trouble from one unit to others could be minimized if units in trouble could be disconnected fast enough from the bus. It is felt that the speed of the d-c breakers must be particularly high to be effective in this regard. This high speed will reduce the time of heavy power inflow to the flashover and clear before the inflow can reach its peak, thereby limiting damage and the extension of the disturbance to other units. With the widespread use of mercury-arc rectifiers in the conversion field, new high-speed switchgear of improved design over that previously available was developed. It was decided that the d-c breakers would be replaced with a modern high-speed breaker of the *FB* type.

The breakers in this installation employ the following tripping schemes:

1. Reverse-current protection by means of the bucking bar releasing the holding armature. As it is necessary to start the machines from the d-c bus, a slowly rising reverse current of approximately 1,600 amperes is permitted to pass safely through the breaker but a higher reverse current or a fast-rising reverse current of a lower value will trip the breaker.

2. Overload trip on the inductive leg, affording machine overload protection and permitting harmless surges to pass through the breaker.

3. Instantaneous high overload protection which will operate the breaker should any rotary converter contribute a larger amount of forward current into a commutator flashover on any of the machines connected to the bus.

4. Shunt trip.

5. Hand trip.

It is felt that this advancement in the state of the art of circuit-breaker design will materially reduce damage in this station during faults and that the time of outage of load may be materially reduced.

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Transient Performance of Induction Motors

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IT has been the usual practice to solve induction-motor transient problems either on the basis of the familiar steady-state equivalent circuit or by calculations which require simplifying assumptions. These methods neglect important factors determining the performance. This paper shows the necessity of considering for many practical problems the electrical and mechanical transient phenomena simultaneously.

For example, during sequential operations such as jogging and plugging, conditions inside the rotor, phase relations, and magnitudes of fluxes and current are influenced both by the electrical transients and by the mechanical slip and oscillation. Also, the residual stator and rotor currents and the angular displacement of the rotor affect performance during plugging; these have been neglected previously.

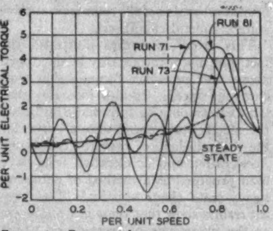

Figure 1. Per unit electric torque versus per unit speed

Effect of rotor deceleration. Transient speed-torque curves with constant rotor deceleration from steady-state conditions at 0.99 per unit speed

Run 71— 6 cycles for unit speed change
Run 81—12 cycles for unit speed change
Run 73—24 cycles for unit speed change

In order to evaluate some of these mutually interrelated phenomena which had not been analyzed previously, the induction-motor equations including the rotor mechanical load and inertia were set up on the General Electric Company differential analyzer.[1] Since it was desired to investigate a number of different operating conditions, the machine characteristics were held constant in all but a few cases. These characteristics given in Appendix B are those of a low-resistance motor which tend to accentuate the motor transients. The effect of increased rotor resistance may be seen in Figure 15. The results and conclusions presented in this paper indicate the importance of considering the transient electrical and mechanical phenomena simultaneously.

Assumptions

The analysis and results are based on the following assumptions (which are similar to those of reference 2).

1. Balanced windings are assumed on both rotor and stator.
2. The mutual inductance between any stator and rotor winding is considered to be a cosinusoidal function of the electrical angle between the axes of the two windings.
3. It is assumed that the rotor is smooth and that the self-inductance of any winding is independent of rotor position.
4. The effects of saturation, hysteresis, and eddy currents are neglected.
5. The machine impedances are assumed to be independent of rotor speed.
6. The machine electrical neutral is assumed ungrounded.
7. In all cases studied, the motor is considered connected to an infinite bus through negligible impedance. The effect of system impedance can be taken into account by increasing the stator impedance, that is, by moving the infinite bus from the terminals of the machine and placing it further back in the system.

Conclusions

1. Large errors may be introduced by using the steady-state speed-torque curve for the calculation of transients.
2. Under transient conditions, motor torque can be developed at speeds above synchronous, and generator torque can be developed at speeds below synchronous.
3. Transient electric torques under shock mechanical loads or sudden electrical disturbances can be several times the steady-state pullout torque.
4. Transient electric-torque oscillations incident to a sudden change in applied voltage are, in general, more severe than those due to a sudden change in mechanical load.
5. The initial rate of change of electric torque with respect to speed following a sudden change in mechanical load is less than the steady-state speed-torque curve would indicate.
6. For a sudden change in applied voltage the transient electric torque is unaffected by the electrical angle at which the voltage is changed, provided that the machine was not initially running open-circuited with trapped flux.
7. Transient-torque oscillations following the sudden application of voltage to a machine containing trapped flux (as in jogging) can be much more severe than the transient torques incident to a short circuit at the motor terminals.
8. With a machine temporarily disconnected from the system and running with trapped rotor flux, there are definite optimum times for reapplication of voltage in order to reduce the torque oscillations.

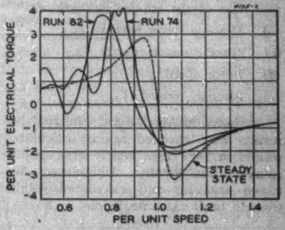

Figure 2. Per unit electric torque versus per unit speed

Effect of rotor deceleration. Transient speed-torque curves with constant rotor deceleration from steady-state conditions at 1.5 per unit speed

Run 82—12 cycles for unit speed change
Run 74—24 cycles for unit speed change

9. The magnitude of transient plugging torque is greatly affected by the magnitude of the trapped rotor flux and its position relative to the stator. It is also affected by motor speed.
10. Transient currents caused by jogging or plugging (with trapped rotor flux) may be larger than the steady-state full-voltage starting current.

Method of Solution

The equations of induction-motor performance (see equations 10 through 20, Appendix A) were set up on the General Electric Company differential analyzer.[1] An inspection of these equations will show that 14 separate integrations were required in the solution. In this respect the problem provided an excellent oppor-

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The authors express their appreciation to S. B. Crary for having suggested this problem and for his constructive criticism; to C. Concordia for his suggestions and discussion; and to members of the staff of the differential analyzer.

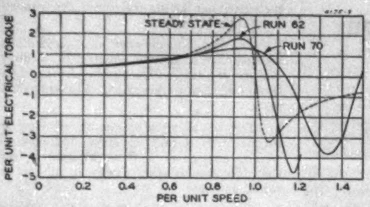

Figure 3 (left). Per unit electric torque versus per unit speed

Effect of rotor acceleration

Transient speed-torque curves with constant rotor acceleration from steady-state conditions at zero speed

Run 70— 6 cycles for unit speed change

Run 62—24 cycles for unit speed change

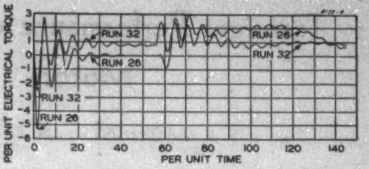

Figure 4 (right). Per unit electric torque versus per unit time (electrical radians)

Effect of voltage disturbance; steady-state full-voltage conditions for 0.99 per unit speed before zero time

Run 32—Voltage reduced to 0.5 per unit at $t=0$. Voltage restored to 1.0 per unit at $t=18\pi$ (9 cycles)

Run 26—Terminals short circuited at $t=0$. Voltage restored to 1.0 per unit at $t=18\pi$ (9 cycles)

tunity to observe and judge the operation of the analyzer, when using the entire integrator capacity. Five quantities, obtained as outputs from the analyzer, were plotted as functions of time. These were:

(*a*). One of the applied voltages, E_α.*

(*b*). Phase α stator current, I_α.

(*c*). Phase β stator current, I_β.

(*d*). Rotor speed, $p\theta$.

(*e*). Electric torque, T_e.

As shown in Appendix C, the analyzer results were checked against calculated results in two cases which were readily soluble analytically. Differences between analyzer and calculated results were in both cases negligible.

Discussion of Results

A. Electric Torques Caused by Rotor Acceleration — Transient Torque-Speed Curves

In order to study the transients due to changing rotor speed as differentiated from those due to electrical disturbances, the machine was subjected to various constant rotor accelerations and decelerations. The electric torques developed in some of these cases are shown in Figures 1, 2, and 3.

In Figure 1, the motor was assumed in all three cases to be operating initially under steady-state conditions with a slip of one per cent. The rotor was then decelerated in runs 71, 81, and 73 at rates corresponding to unit change of speed in 6, 12, and 24 cycles, respectively, of fundamental frequency. It is of interest to note that the initial rate of change of torque with speed is zero and that the electric torque oscillates about the steady-state speed-torque curve. That the initial rate of change of torque with speed is zero, of course, is due to inductive lags in the motor. The relation of the first swings of these curves may be explained as follows:

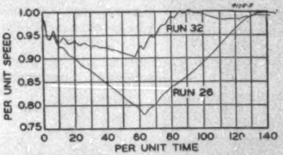

Figure 5. Per unit rotor speed versus per unit time (electrical radians) for cases shown in Figure 4

1. If the rotor were decelerated at an infinitely slow rate, the torque would follow its steady-state speed-torque curve.

2. If the rotor were decelerated at an infinitely fast rate, the torque would remain constant at its initial value because of inability of the motor to respond electrically to the infinite deceleration.

It is seen, then, that the speed-torque curves of runs 71, 81, and 73 appear in a logical sequence between the limiting curves resulting from zero and infinite deceleration.

Figure 1 also shows that appreciably higher-peak electric torque may be developed upon the sudden application of mechanical load than the steady-state theory would indicate, and at a different speed.

Figure 2 illustrates results obtained when the motor was decelerated from initial steady-state conditions for a rotor speed of 1.5 (per unit).

The curves shown in Figure 3 resulted from acceleration of the rotor from steady-state standstill conditions.

This group of curves illustrates the fact that motor torque may be developed above and generator torque below synchronous speed. It also indicates that large errors may be introduced by using the steady-state speed-torque curve for the calculation of severe transients. Acceleration and deceleration tests have been proposed as a method of obtaining steady-state characteristics.[3] The present results show that there is a limitation on the magnitude of acceleration which can be used in such tests if the true steady-state behavior is to be obtained.

B. Electric Torques Due to Sudden Change in Applied Voltage

Figure 4 shows the effect upon electric torque of sudden changes in applied voltage. For both runs 32 and 26 of Figure 4, the motor was initially operating at steady-state conditions with a slip of one per cent. A constant mechanical load torque was assumed in both cases, although arbitrary load-torque curves as a function of speed easily could have

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* See Appendix B for list of nomenclature.

Shows one cycle of steady-state current before zero time

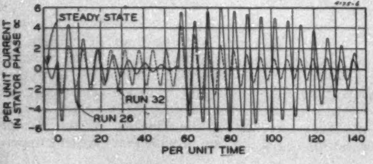


Figure 6 (left). Per unit stator current in phase α versus per unit time (electrical radians) for cases shown in Figure 4

Shows one cycle of steady-state current before zero time

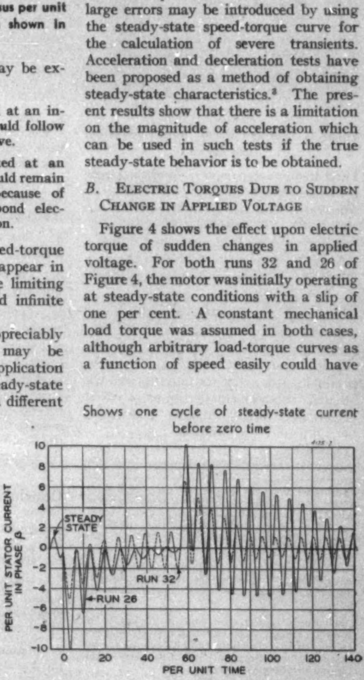
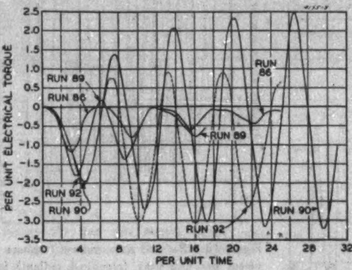


Figure 7 (right). Per unit stator current in phase β versus per unit time (electrical radians) for cases shown in Figure 4

been used if desired. An inertia constant (H) of approximately 0.45 was assumed for motor and load combined.

In run 26, at $t=0$, (also $E_\beta=0$), the applied voltage suddenly was reduced to zero, corresponding to a zero-impedance short circuit at the motor terminals. As is seen from an inspection of Figure 4, the maximum electric torque following the application of a short circuit is of the order of five per unit. It is also seen, as would be expected, that the electric torque became zero after the transients had died out and remained zero until the short circuit was removed. The fault was removed nine cycles after its application. The torque recovery following the removal of short circuit also is pictured in Figure 4.



Effect of rotor speed on plugging torque. Infinite-inertia machine running at various positive speeds with zero rotor flux. Unit voltage of negative phase rotation applied at zero time

Run 86—Rotor speed = 1.00 per unit

Run 89—Rotor speed = 0.50 per unit

Run 92—Rotor speed = 0.15 per unit

Run 90—Rotor speed = 0 per unit

Figure 8. Per unit electric torque versus per unit time (electrical radians)

Run 32, which also has been plotted in Figure 4 for purposes of comparison, is similar to run 26, except that the applied voltage was reduced suddenly to half value rather than to zero.

Several runs also were made in which the electrical angle at which the voltage was reduced or restored was varied. As stated in the conclusions, the electrical angle of switching under these conditions had no effect on the ensuing electric torque for a given switching time.

Figure 5 shows the per unit speed as a function of time for runs 26 and 32. The two-phase stator currents I_α and I_β for these two runs are plotted in Figures 6 and 7, respectively. As shown in Appendix A, corresponding three-phase currents may be obtained from I_α and I_β by means of a relatively simple manipulation; in fact, in the present treatment I_α itself is one of the three-phase currents.

C. PLUGGING TORQUES

Two extreme plugging conditions are those in which the machine is plugged instantaneously while running at rated speed and load, and while it is running at rated speed but after the rotor flux has died out completely. Plugging torques from runs which are nearly these extremes are shown in Figure 10.

Run 85 was obtained as follows:

1. With the motor running at steady-state conditions with a slip of one per cent, the stator phase β was opened at a zero of I_β.
2. Phase α of the stator was opened approximately one-fourth cycle later at a zero of I_α.
3. At the same instant that phase α was opened, voltage of opposite (negative) phase rotation was applied simultaneously to both stator phases. (In this case, the mechanical load torque was assumed to be constant at its initial value, and the inertia constant (H) was assumed to have a value of approximately 1.43.) This is obviously almost one of the extreme cases mentioned before.

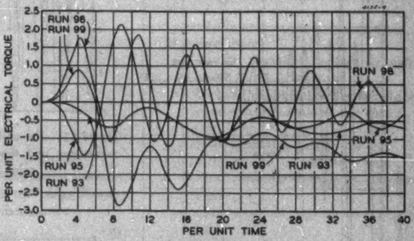


Figure 9. Per unit electric torque versus per unit time (electrical radians)

Effect of rotor speed on plugging torque. Infinite-inertia machine running at various negative speeds with zero rotor flux. Unit voltage of negative phase rotation applied at zero time

Run 95—Rotor speed = −0.25 per unit

Run 93—Rotor speed = −0.50 per unit

Run 99—Rotor speed = −0.75 per unit

Run 98—Rotor speed = −1.00 per unit

The two phase currents for this run are on Figures 11 and 12.

Run 86 was obtained by applying negative-phase-rotation voltage to an infinite-inertia machine running at synchronous speed with zero rotor flux. It is the other extreme case and is compared with run 85. The effect of trapped rotor flux is seen to be very important.

It is interesting to note the similarity between run 86 of this paper and run 36 of reference 4, in which the machine constants are similar to those used in this study.

Figures 8 and 9 have been included to show the effect of rotor speed upon plugging torque. All curves shown in these two figures are based on the assumptions that the motor was initially unexcited, contained no trapped flux, and had infinite rotor inertia, negative phase rotation of voltage being applied in all cases.

Figure 9 shows torques which are not, strictly speaking, "plugging" torques since the phase rotation of the applied voltage is in the same direction as the motor rotation. However, they are of interest in that they extend the range of rotor speed. Such conditions may be encountered in some pump and propeller drives having more than one motor on the same shaft.

D. RECLOSING OF STATOR CIRCUIT WITH TRAPPED FLUX IN THE ROTOR

Several cases were studied in which the stator was open-circuited from a steady-state full-load running condition. After several cycles, full voltage was reapplied. Figures 13 and 14 show the electric torque and speed respectively for a few of these cases. It will be seen from these curves that there is at least one definite optimum reclosing time both from the standpoint of torque oscillations and from that of speed-recovery time. That there are several of these optima, and that they depend on machine inertia and rotor resistance may be seen from the following analysis.

In the steady state with the motor running at approximately one per cent slip, the rotor flux rotates at synchronous speed and lags the stator flux by a definite angle. It is continually slipping forward with respect to the rotor. When the stator is open-circuited, the rotor flux becomes fixed in the rotor and decays relatively slowly (with the rotor open-circuit time constant L/r_r) from its initial value. Since the electric torque is zero, the rotor slows down so that the trapped and slowly decaying rotor flux lags at an increasing rate behind the steady-state position of the stator flux.

An understanding of the phenomena occurring when the stator circuit is again closed on the supply voltage may be obtained by regarding the induction motor as a synchronous machine running below synchronism, the trapped rotor flux taking the place of the synchronous-machine excitation. The sudden reapplication of stator voltage produces two fluxes, one very nearly the steady-state rotating field, the other a stationary (and decaying) field. Now the machine time constants are much shorter than with the stator open-circuited, so that the stationary field components decay more rapidly, but for the purposes of the argument one may neglect this decay. Both stator fields react on the rotor flux, the steady-state rotating component producing a slip-frequency torque, the stationary component producing a speed-frequency component. If the slip is small, the slip-frequency component acts roughly as a "synchronizing" torque, so that it is evident that there is a preferred initial position of the rotor for minimum time to recover speed, while the speed-frequency component produces only a superposed vibration of the rotor speed. One may calculate from the initial slip and rate of increase of slip the relative angle through which the rotor has passed, and it then will be found that at first for very small angles of retardation the speed will recover quickly and with a minimum of disturbance. Then, as the angle exceeds 90 degrees, the initial accelerating torque becomes smaller and finally is not sufficient, so that the machine speed at first may decrease further by an appreciable amount. As in the equivalent synchronous-machine case, the "worst' angle lies only a relatively short distance behind the "best" angle. The situation is made more complex by the fact that in every slip cycle the slip becomes greater and the trapped flux becomes smaller, so that the variation with angle becomes much less pronounced. The present case has a rather low rotor

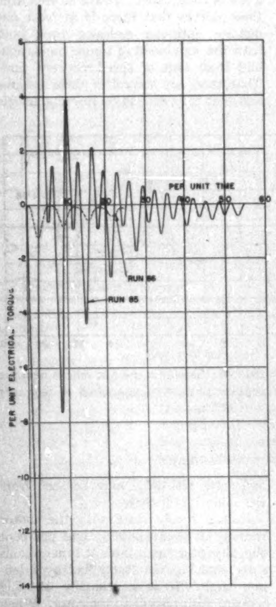


Figure 10. Per unit electric torque versus per unit time (electrical radians)

Effect of trapped rotor flux on plugging torque

Run 85—Plugging from steady-state conditions for 0.99 per unit speed (see section C)

Run 86—Plugging an infinite-inertia machine running at synchronous speed with zero rotor flux

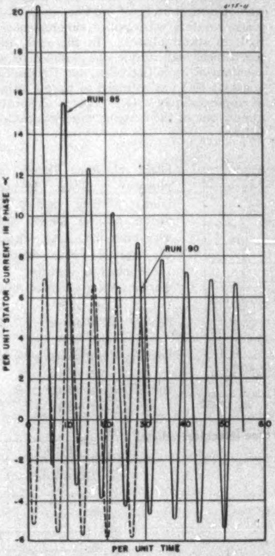


Figure 11. Per unit stator current in phase α versus per unit time (electrical radians) for runs 85 and 90

See Figures 10 and 8, respectively, for corresponding electric torque

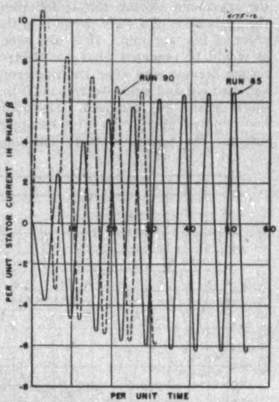


Figure 12. Per unit stator current in phase β versus per unit time (electrical radians) for runs 85 and 90

See Figures 10 and 8, respectively, for corresponding electric torque

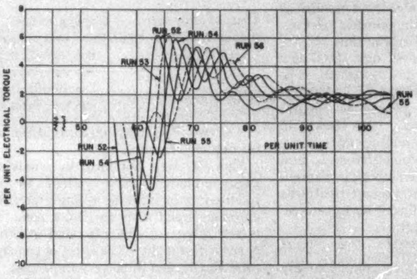


Figure 13. Per unit electric torque versus per unit time (electrical radians)

Effect of switching angle. Steady-state full-voltage conditions for 0.99 per unit speed before zero time. Stator open-circuited at $t=0$

Run 52—Voltage restored to 1.0 per unit at $t=18\pi$ (9 cycles)

Run 53—Voltage restored to 1.0 per unit at $t=18.5\pi$ (9$^1/_4$ cycles)

Run 54—Voltage restored to 1.0 per unit at $t=19\pi$ (9$^1/_2$ cycles)

Run 55—Voltage restored to 1.0 per unit at $t=19.5\pi$ (9$^3/_4$ cycles)

Run 56—Voltage restored to 1.0 per unit at $t=20\pi$ (10 cycles)

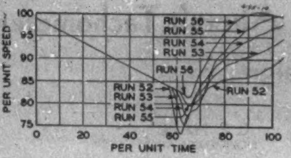


Figure 14. Per unit speed versus per unit time (electrical radians) for runs 52–56

See Figure 13 for corresponding electric torque

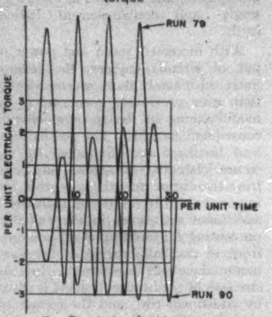


Figure 15. Per unit electric torque versus per unit time (electrical radians)

Effect of rotor resistance. Full voltage applied to locked rotor machine

Run 79—Rotor resistance = 0.04 per unit. Positive-phase-rotation voltage

Run 90—Rotor resistance = 0.01 per unit. Negative-phase-rotation voltage

resistance, so that the effect is still large in the second slip cycle which has been studied. The inertia of the machine evidently enters here also, as it determines the rate of increase of slip. A motor with a small inertia and a high rotor resistance would show little variation with angle, and the ratio of inertia to rotor resistance may form a rough criterion of the importance of the effect of angle. Also if the slip becomes very large, the effect of the stationary stator flux may be emphasized and a pronounced speed decrease consequently may be observed. In all cases, the severity of the rotor swings should tend to be less than in the synchronous-machine case because of the generally higher resistances and the fact that the rotor has no sustained rotor excitation and thus no preferred position in which it must come to equilibrium.

E. TRANSIENT CURRENTS

If we consider only the a-c component of current, it is seen from Figures 6 and 7 that the current caused by a terminal short circuit is somewhat less than full-voltage starting current; however, that resulting when the fault is removed is approximately equal to full-voltage starting current.

It is interesting to note that the current caused by plugging (when the rotor contains trapped flux) may be much larger than full-voltage starting current. For the case illustrated in Figures 11 and 12, the maximum current is nearly twice the normal starting value.

Appendix A. System Equations

The equations whose solution forms the basis for the results of this study have been derived by Stanley.[3] These equations in which all quantities have been referred to the stator may be written

$$e_\alpha = p\psi_\alpha + r_r i_\alpha + \psi_\beta(p\theta) = 0 \qquad (1)$$

$$e_\beta = p\psi_\beta + r_r i_\beta - \psi_\alpha(p\theta) = 0 \qquad (2)$$

$$\psi_\alpha = G(p)E_\alpha + x(p)i_\alpha \qquad (3)$$

$$\psi_\beta = G(p)E_\beta + x(p)i_\beta \qquad (4)$$

$$T_e = \psi_\beta i_\alpha - \psi_\alpha i_\beta \qquad (5)$$

$$p\theta = \frac{1}{H'}\int (T_e - T_m)dt \qquad (6)$$

$$\theta = \int p\theta dt \qquad (7)$$

$$E_\alpha = E\cos t \qquad (8)$$

$$E_\beta = E\sin t \qquad (9)$$

where

$$G(p) = \frac{M}{Lp + r_s}$$

$$x(p) = \frac{L(x'p + r_s)}{Lp + r_s}$$

$$x' = L - \frac{M^2}{L}$$

All quantities are defined in Appendix B.

For solution on the differential analyzer, these equations are written in the following form:

$$\psi_\alpha = -r_r\int i_\alpha dt - \int \psi_\beta d\theta \qquad (10)$$

$$\psi_\beta = -r_r\int i_\beta dt + \int \psi_\alpha d\theta \qquad (11)$$

$$x' i_\alpha = \psi_\alpha + \int\left(\frac{r_s}{L}\psi_\alpha - \frac{M}{L}E_\alpha - r_s i_\alpha\right)dt \qquad (12)$$

$$x' i_\beta = \psi_\beta + \int\left(\frac{r_s}{L}\psi_\beta - \frac{M}{L}E_\beta - r_s i_\beta\right)dt \qquad (13)$$

$$E_\alpha = -\int E_\beta dt \qquad (14)$$

$$E_\beta = \int E_\alpha dt \qquad (15)$$

$$T_e = \int \psi_\beta di_\alpha + \int i_\alpha d\psi_\beta - \int \psi_\alpha di_\beta - \int i_\beta d\psi_\alpha \qquad (16)$$

$$p\theta = \frac{1}{H'}\int (T_e - T_m)dt \qquad (17)$$

$$\theta = \int p\theta dt \qquad (18)$$

Equations 10 through 18 which require 14 integrators were set up on the differential analyzer, and a number of solutions was obtained. The quantities plotted were electric torque, rotor speed, applied voltage, and the two components of stator current. The latter do not appear explicitly in equations 10 through 18 but come directly from the definition of flux; namely:

$$I_\alpha = \frac{1}{M}(\psi_\alpha - Li_\alpha) \qquad (19)$$

$$I_\beta = \frac{1}{M}(\psi_\beta - Li_\beta) \qquad (20)$$

From the definitions of the α and β quantities,[3] the stator-phase currents for a three-phase machine in terms of I_α and I_β are (with no zero-sequence currents):

$$i_a = I_\alpha \qquad (21)$$

$$i_b = -\frac{1}{2}I_\alpha + \frac{\sqrt{3}}{2}I_\beta \qquad (22)$$

$$i_c = -\frac{1}{2}I_\alpha - \frac{\sqrt{3}}{2}I_\beta \qquad (23)$$

Appendix B. Nomenclature and Values of Parameters for Machine Studied

All quantities are in per unit on kilovolt-ampere base.

L = apparent three-phase stator and rotor self-inductance = 3.07
M = apparent three-phase mutual inductance = 2.99
r_s = resistance of one stator phase = 0.01
r_r = resistance of one rotor phase = 0.01
$p\theta$ = rotor speed
t = time in electrical radians at unit speed
θ = electrical angle between rotor phase α and stator phase α at any time t
$p = d/dt$
I_α = stator current in positive α axis
I_β = stator current in positive β axis
E_α = stator terminal voltage in positive α axis
E_β = stator terminal voltage in positive β axis
E = maximum value of terminal voltage
ψ_α = rotor-flux linkages in positive α axis referred to stator
ψ_β = rotor-flux linkages in positive β axis referred to stator
i_α = rotor current in positive α axis referred to stator
i_β = rotor current in positive β axis referred to stator
e_α = rotor terminal voltage in positive α axis referred to stator = 0
e_β = rotor terminal voltage in positive β axis referred to stator = 0
H' = rotor moment of inertia = 338.0
H = conventional inertia constant = $H'/4\pi$ (frequency) = 0.448
T_e = induction-motor torque
T_m = mechanical load torque = 0.91
i_a = stator current in phase a
i_b = stator current in phase b
i_c = stator current in phase c
λ_1, λ_2 = roots of characteristic equation (locked rotor)

$\omega = 2\pi \times$ fundamental applied frequency $= 1.0$

$\phi =$ power-factor angle (locked rotor)

$$= \tan^{-1} \frac{\lambda_1\lambda_2 - \omega^2}{\omega(\lambda_2 + \lambda_1)}$$

$$\theta_1 = \tan^{-1} \frac{\omega^2 + \lambda_1\lambda_2}{\omega(\lambda_2 - \lambda_1)}$$

Appendix C

As a check on the differential-analyzer accuracy, the torque-time curves of two of the simple cases were checked by calculation. An exact expression for the electric torque with blocked rotor and simultaneous application of voltage in both axes (on the basis of the given assumptions) is found in equation 5 of reference 5. This equation may be reduced to

$$T_e = T_0\left[1 + e^{-(\lambda_2\lambda_2)t} - \frac{e^{-\lambda_1 t}\cos(\omega t + \theta_1) + e^{-\lambda_2 t}\cos(\omega t - \theta_1)}{\cos\theta_1}\right]$$

where

$$\theta_1 = \tan^{-1} \frac{\lambda_1\lambda_2 + \omega^2}{\omega(\lambda_2 - \lambda_1)}$$

This is identical in form with the approximate equation 11 of reference 6, the difference being found in the phase angle θ_1 which replaces ϕ of Kilgore and Wahl[6] where ϕ is

$$\phi = \tan^{-1} \frac{\omega^2 - \lambda_1\lambda_2}{\omega(\lambda_2 + \lambda_1)}$$

This difference amounts to three per cent in the maximum torque for the machine constants used in the greater part of the study. The calculation of torque was carried out for four or five cycles of each of two cases (runs 90 and 79) Figure 15, and the discrepancy between calculated and analyzer solutions was found to be negligible. The two-phase currents corresponding to run 90 are given in Figures 11 and 12.

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Recent Advances in Aircraft Tachometer Design

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Synopsis: A discussion of recent developments in magnetic-drag-type aircraft tachometer equipment. Wartime demands for increased temperature range, instrument durability, and compactness have led to certain revisions and new design features. The results of these recent developments are outlined in this paper.

THE magnetic-drag tachometer system consists of a three-phase generator supplying power to a synchronous motor in an indicator (Figures 1 and 2). The generator is mounted directly on the airplane engine and the indicator is mounted on the instrument panel. The motor in the latter revolves an assembly of permanent magnets which imparts torque to an aluminum-alloy drag disk, thereby moving the indicator pointer over the scale until an equal and opposite restraining torque is developed in the control spring.[1] The generators and indicators are interchangeable with other instruments of the same manufacture and similar units made by other concerns.

During the past three years several new requirements have influenced the design and manufacture of this aircraft tachometer equipment: the operating temperature range has increased by nearly 50 per cent; the vibration conditions which these devices must withstand have become more severe with the production of faster and more powerful engines; and increased instrumentation has made better utilization of panel space essential. Revisions in generator and indicator design have resulted from the new temperature and vibration requirements. A new dual-element tachometer, occupying the space as one single indicator, has been developed to save panel area.

Generators

A cut-away view of a tachometer generator is illustrated in Figure 3; this readily illustrates the simple, sturdy construction of the generator. To obtain satisfactory operation at temperatures down to −65 degrees centigrade, a special low-temperature lubricant is now used in the ball bearings. The resulting torque required to drive the generator at −65 degrees centigrade is shown in Figure 4 for single- and double-element indicator loads.

With increased speed and power output of aircraft engines, the generator must withstand more severe vibration than ever previously encountered; and modifications in design now eliminate consequent hazards. The possibilities of lead breakage are eliminated by more secure clamping of leads; oil leakage from the engine into the generator winding is avoided by better oil seals and by additional vent-holes in the end-shield, preventing the build-up of oil pressure in front of the ball bearing at the flange-mounted end of the generator. Increased strength of the front end shield is assured by additional ribs, and the former two-piece receptacle end-shield is now a stronger and simpler one-piece casting.

The tachometer generator drive key is required to withstand accelerations of 400 revolutions per second per second. Generators of the present construction have withstood repeated accelerations as great as 800 revolutions per second per second without damage. In certain installations where unusually severe pulsations of the driving system are present, the drive-key design is further modified to prevent excessive generator wear and resulting pointer fluctuations in the indicator. This drive key includes a short section of flexible tachometer cable. The square driving tang is split diagonally through the center line and the two sections are spread so that no play is present between the drive key and the mating part on the engine. By this change in construction no indicator pointer fluctuation occurs after many hours' operation where previously the indication became unsatisfactory during the first 10 to 20 minutes of use.

Representative generator-voltage-output–speed curves are shown by Figure 5 for conditions of no load, one and two indicator element loads.

Indicators

The range of operating temperature for tachometer indicators has increased, upward from +45 degrees centigrade to +70 degrees centigrade, and downward from −35 degrees centigrade to −55 degrees centigrade. This increase in range

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Figure 1. Tachometer equipment

Left to right: Tachometer generator; single-element indicator, $3^1/_2$ revolutions; dual-element indicator; single-element indicator (330 degrees); synchroscope

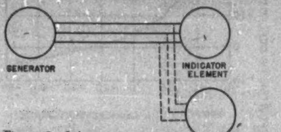

Figure 2. Schematic connection diagram

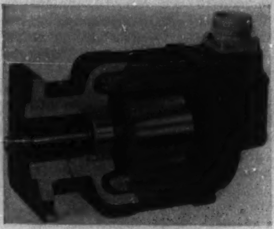

Figure 3. Cut-away view of generator

from a total of 80 to a total of 120 centigrade degrees requires improved temperature compensation material so that the error in indication is not excessive.

Improvements in commercially available temperature-sensitive nickel-steel compensator material provide the typical temperature compensation curve shown in Figure 6; the uncompensated curve shows the change in indication (at the 3,000-rpm point) before the addition of compensator material, and the compensated curve shows that this error is reduced to less than one per cent by proper compensation.

Due to the high-ambient-temperature operation, it is essential that the lubricant for the indicator motor bearings have a low evaporation rate. To meet this requirement the indicator ball bearings are lubricated with inhibited blackfish jaw oil applied in controlled quantities. With this lubrication individual indicators have operated continuously for as much as 20,000 hours.

Room - temperature torque – speed curves for representative tachometer indicator motors are shown in Figure 7. The starting or pull-in torque is developed jointly by a permanent-magnet rotor and eddy-current hysteresis disk.

The vibration test for tachometer indicators consists of movement of the indicator on a 0.018–0.020-inch-diameter circle in a plane inclined 45 degrees to the horizontal. The top frequency vibration has been increased from 2,500 to 3,000 cycles per minute. All indicators are subjected to this vibration test after assembly to assure freedom from excessive pointer oscillations or changes in indication.

Dual-Element Indicator

As airplanes increased in size and complexity, the available panel space has been needed for more and more indications; thus, where previously a single-element indicator was installed it has become essential to build two or more indicators in one case wherever possible. This need leads to the development of the dual-element tachometer indicator, shown in Figure 1, to show the speed of two engines in the same panel space as was previously required for one engine. A cutaway view of this dual indicator, with one element removed, is shown in Figure 8.

The dual indicator presented problems of much smaller-size motors, smaller rotating-magnet assemblies, and a means of coupling the two elements to a concentric pointer assembly. Previous experience with the single-element indicator showed the following adjustments to be desirable:

(*a*). Zero adjustment.

(*b*). Full-scale adjustment.

(*c*). Balancing means.

(*d*). Adjustable temperature compensation.

In addition cam adjustment of gear mesh has been built into each element of the dual indicator to facilitate adjustment and maintenance.

A special feature of the design is the possibility of disassembly into interchangeable subassemblies. These subassemblies may be adjusted, tested, and inspected as units and subsequently assembled into completed tachometers. The motor and magnetic-drag assembly are completely assembled and tested in the normal manufacturing procedure, thereby preventing expensive tear-downs at later positions. Field maintenance and handling is greatly facilitated by these types of construction.

Smaller Motor

For the synchronous motor of the dual indicator, a recently developed magnetic material, copper-nickel-cobalt, is used for the permanent-magnet rotor instead of the sintered-oxide material utilized in the single-element indicator. The new material offers the advantages of improved mechanical strength, greater ease of fabrication, and simpler assembly.

The stator of the indicator motor is of the conventional three-phase type. To decrease the temperature rise of the windings through greater thermal conductivity, glass-cloth slot insulation replaces the fiber type common to the single-element indicator. This change reduces the temperature rise by approximately ten per cent.

Drag Magnet Assembly

The necessity of providing two indicator elements in the space previously occupied by one necessitates smaller rotating magnets. In order to obtain sufficient torque for the moving element assembly, a second new magnet material is utilized. This material, Alnico V, produces approximately twice the external energy per unit volume of Alnico II used in the single-element indicator. Major differences in handling the two types of Alnico in manufacturing are the necessity for initial magnetization of the Alnico V during cooling from a semimolten state, and the requirement that Alnico V be carried through a much greater temperature stabilization cycle than for Alnico II in order to prevent sub-

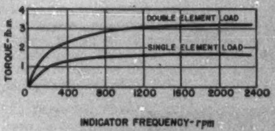

Figure 4. Generator driving torque at –65 degrees centigrade

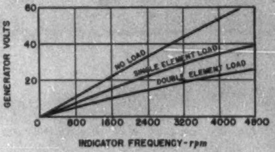

Figure 5. Generator voltage output

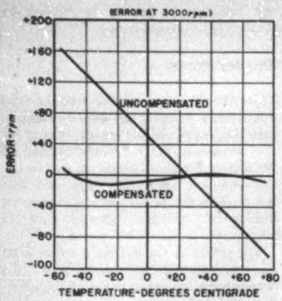

Figure 6. Typical temperature compensation

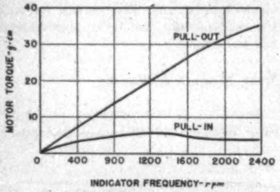

Figure 7. Indicator-motor torque

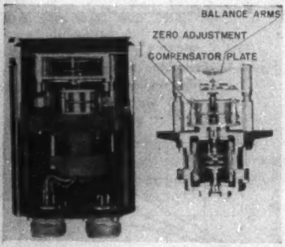

Figure 8. Cut-away view of dual indicator

sequent changes in magnetic strength throughout the instrument operating temperature range of −55 to +70 degrees centigrade.[2,3]

Calibration adjustment of the indicator may be accomplished either by magnetic knockdown with an a-c coil or by varying the gap between the two sets of magnets. The latter adjustment is normally used for only small changes in calibration and is readily made with the instrument cover removed. Calibration adjustment with a demagnetizing coil may be made to either element of the assembled indicator without affecting the calibration of the other element, although the magnets in the two assemblies are separated by less than three quarters of an inch. This method is ideal since the element being adjusted may be in operation while the demagnetizing is being done. Temperature compensation adjustment is provided by movement of the compensator plate (Figure 8) along the length of the magnets in order to shunt more or less flux.

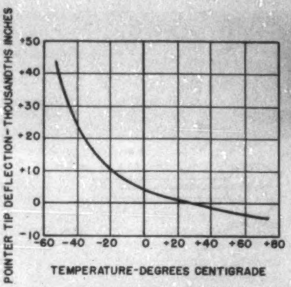

Figure 9. Temperature–deflection characteristics of single-coated pointers

Moving Elements

The zero-adjusting arm and balance arms are readily discernible in Figure 8. The mechanical assembly of gear hubs to shafts as small as 0.031 inch in diameter is accomplished by means of a knurl on the shaft; thus assuring a tight, permanent fit.

The concentric pointer shaft assembly (Figure 8) is supported at the ends by ring-stone jewels. Intermediate supports are provided by two additional ring-stone jewels mounted in the outer bearing sleeve with the first one supporting the front end of the inner shaft and the second the back end of the outer sleeve. The advantages of this assembly are its inherent line-up and a single end-play adjustment for the complete subassembly.

Prior to the increase in range of temperature operation, tachometer pointers were normally flat and painted with luminescent material on one side only. Changes in temperature cause bending of the pointer because of the difference in expansion and contraction of the aluminum pointer material and the fluorescent material. The amount of deflection occurring for a 0.010-inch-thick pointer $1^3/_{16}$ inches in length is shown in Figure 9. In order to improve this condition the pointer for the single-element indicator has been "ribbed" to reduce this bending action to less than half of its former value. Because of the small space between the two pointers on the dual element indicator, its pointers are made flat but are painted on both front and back sides, thus eliminating virtually all deflection.

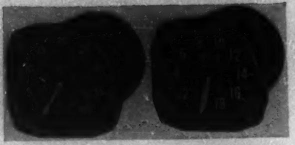

Figure 10. Dual tachometers for supercharger and helicopter applications

Conclusion

The several problems of increased temperature range, additional vibration, and superior panel utilization have been met. Existing designs have been improved and new designs developed to accomplish the stringent wartime requirements.

In addition to the furthered development in magnetic materials and processes fostered by the designing of the tachometer equipment, certain unique applications have been made. Among the present and future applications are included airplane supercharger and helicopter rotor speed indication (Figure 10), test panel equipment, regulator control, and timing apparatus. A single indicator element from the dual tachometer can be built into a $1^7/_8$-inch diameter case for use where reduced size is paramount, and the separate indication of engine speed is desired.

By means of these design revisions it has been possible to meet the new requirements which aircraft tachometer systems have encountered.

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Automatic Control and Switching Equipment for Capacitor Banks and Its Application

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THE increasing use by utilities or industrial plants of shunt capacitors for voltage improvement, power-factor correction, and release of circuit capacity is leading in many cases to the desirability of automatically switching or controlling the capacitors.

On some utility and industrial system circuits, capacitors have been or are being applied to the extent that it becomes desirable to switch part of them at time of light load to control voltage and to minimize circuit losses.

Recent studies[1] have shown that as systems grow, capacitors on distribution feeders and in substation banks have a definite place in the growth process. There should be, therefore, an increasing demand for automatic control equipment in this connection. Where industrial plants are encouraged, by means of the power-factor clause in the utility rate structure, to supply part of their own magnetizing kilovars, it sometimes may be desirable to switch the capacitors. This paper offers application information as a guide to the selection of proper automatic switching equipment, and describes available types of control equipment.

Reasons for Switching Capacitors Automatically

Capacitors applied for voltage-regulation purposes should be automatically controlled where manual switching is not convenient or reliable. Switching also may be necessary or desirable to avoid overvoltage or excessive losses, particularly during light-load periods for capacitors used to reduce circuit loadings, or to meet the requirements of the power-factor clause of an electric-power rate.

Switching usually need not be considered if the minimum-load kilovars exceed the capacitor kilovolt-amperes. It is shown in the appendix that circuit losses will be a minimum if the unswitched capacitor kilovolt-amperes equals the average kilovar requirement of the load, that is, the average ordinate of the daily kilovar-load pattern. In some cases automatic switching equipment cannot be justified economically if losses are the only consideration.

Types of Equipment

In addition to the capacitors, automatically controlled capacitor equipment consists of:

A. Switching device. For convenience, this may be divided into the following classes of application:

1. Low-voltage circuits (600 volts or less)
2. Medium-voltage circuits (2,400 to 13,800 volts)
3. Utility distribution feeder circuits.

B. Control equipment for automatically controlling the switching device associated with the block of capacitors.

MAIN SWITCHING DEVICE

1. Switching Equipment for Low-Voltage Circuits. This voltage class (up to 600 volts) is encountered most generally in industrial plants, with the nominal 460-volt circuit being most common. The two types of equipments for this service are air circuit breakers and contactors.

Standard air circuit breakers generally are used, and have the advantage of providing fault protection in addition to the switching function. Breakers are not designed for highly repetitive operation, but in the average industrial plant the number of operations is not large enough to result in a serious maintenance problem. In those installations where the number of operations is large, contactors are the logical selection and are available with or without fuses.

2. Switching Equipment for Medium-Voltage Circuits. Standard power circuit breakers are used almost always as the switching device for power circuits in the voltage range of 2,400 to 13,800 volts. Contactors are available for 2,400- and 4,160-volt service.

3. Switching Equipment for Use With Utility Distribution Circuits. This class of switching equipment is limited almost entirely to the utility field for use with small banks of capacitors in which the switching device is primarily a switch only and not a breaker; fault protection is provided by other means. Here a power breaker could not be justified economically for the small capacitor banks (45 to 180 kva) located on distribution circuits. Oil switches for pole mounting are available for this service in ratings up to 100 amperes and 5,000 volts. A typical unit-type equipment, including capacitors, switch, and control for a 180-kva bank is shown in Figure 1.

AUTOMATIC CONTROL EQUIPMENT

The control is the heart of the equipment; it receives the signal, interprets it, and then sets into operation the proper devices to perform the desired function. This control consists fundamentally of the following:

1. *Master Element.* This is the "brain," and may be a voltage-, current-, kilovar-, or power-factor-sensitive device.
2. *Time-Delay Device.* A time-delay device is a practical necessity to avoid unnecessary operation of the switching device for signal changes that are only momentary. This may be a separate device or may be incorporated as part of the master element.
3. *Sequencing Device.* This is necessary only for multistep control, and determines the order in which the capacitor groups are switched in and out of service.

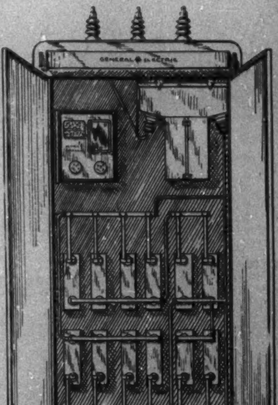

Figure 1. Unit-type pole- or base-mounted automatically controlled capacitor equipment, 180 kva, 2,400 volts

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4. *Miscellaneous Relays or Devices.* A lockout device is required to prevent the switching device from reclosing on a fault. It is also common to include, for convenience of control, an "automatic–manual" transfer switch and a "close–trip" switch for each capacitor block.

Control power is standardized at 220 volts alternating current or 125 volts direct current, following switchgear and control practice.

The simplified control schemes for the two most common controls are shown in Figures 2 and 3. To illustrate the function of each of the component parts, the detailed performance of a typical control will be outlined. The sequence for a single-step voltage-sensitive control is quite straightforward and will not be discussed. It should be noted that, in accordance with standard practice, the devices are shown in their de-energized position.

Figure 3 shows the schematic diagram and Figure 4 a control panel for a three-step kilovar control. The sequence of operation is as follows: As the load magnetizing kilovars increase and reach a predetermined value, the master element operates, initiating the time-delay device and establishing a closing circuit for the correct direction of the sequencing switch. If, after a preset time delay, the master element still indicates capacitors are needed (that is, the signal was not the result of just a momentary disturbance), the time-delay device operates and starts the sequencing device. Once the sequencing device is started its motor continues to operate until the first position is reached, when a set of contacts closes to initiate the closing circuit of the switching device. The same control-sequence procedure is followed for switching in successive blocks. When the load kilovar requirements decrease, the capacitors are switched out of service in the opposite sequence, that is, the last capacitor group switched into service is the first group switched out of service.

If the signal to the master element does not last for the full time-delay period, the control is arranged so the timing device will be reset for a full timing period for its next operation.

Master Control Elements

There are two characteristics common to all regulating types of master elements that should be fully understood to select intelligently the proper device. These are the range of adjustment and the band width.

The range of adjustment refers to the normal operating level of the device, and is necessary, for practical purposes, to allow for differences in operating conditions. For example, the system voltage level may vary over a rather broad range, so the normal setting of a voltage-sensitive device must be capable of adjustment.

Band width is defined as the difference between pickup (switching a capacitor on) and dropout (switching a capacitor off). *The band width must be larger than the change in signal caused by switching the largest block of capacitors to prevent pumping action of the control and switching equipment.*

The most common types of master-control elements are:

(*a*). Voltage.
(*b*). Current.
(*c*). Kilovar.
(*d*). Power factor.
(*e*). Time switch.

(*a*). *Voltage-Sensitive Device.* This is essentially a contact-making voltmeter; the most common type is that used with voltage regulators. Standard voltage-sensitive devices are available with a range of adjustment from 90 to 110 per cent, and with a band width adjustable from three to seven and a half per cent. A multistep control requires the lower band width, as it is likely to be used for a regulating duty where fine control is necessary.

Table I. Application of Automatic Capacitor Switching Control

	Switching Function										
				Switching Capacitors on Basis of Power-Factor Clause Requirements* Power Factor for Billing Determined By							
				Average Monthly Power Factor			Power Factor at Maximum Kw Demand				
Type of Master Element	Removal of Capacitors at Light Load	Regulating Voltage	Regulating Kilovars	Occasional Measurements	Continuous Measurements	Kwhr and Kvarhr Comparison	Occasional Measurement**	Continuous Measurement of Power Factor	Kwhr and Kvarhr Comparison	Billing on Kw and Kvar Demand	Billing on Kva Demand
Voltage, single-step	O.K.				Leave capacitors unswitched	Leave capacitors unswitched					
Voltage, multistep	Usually not necessary	O.K.									
Current, single-step	O.K. if power factor versus load characteristic known										
Current, multistep											
Kilovar, single-step	O.K. where voltage magnitude not satisfactory signal			O.K.			O.K.	O.K.	O.K.	O.K.	O.K.
Kilovar, multistep	Usually not necessary		O.K.	O.K. where graded control desired			O.K. where graded control desired →				
Power factor, single-step				O.K. but desensitizing control required			O.K. but desensitizing control required →				
Power factor, multistep				O.K. where graded control desired but desensitizing control required			O.K. where graded control desired but desensitizing control required				

* Where capacitors are used for rate purposes only, they need not be switched unless there is a penalty for leading power factor. It may be desirable to switch capacitors at reduced loads to prevent excess voltage and losses

** At or near maximum load.

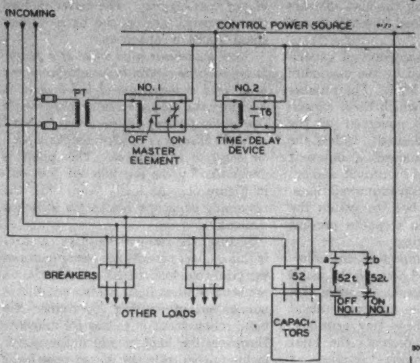

Figure 2. Simplified schematic connection diagram showing single-step voltage control

Devices:
1—Master element
2—Time-delay device
52—Capacitor switching device (breaker)
PT—Potential transformer

(*b*). *Current-Sensitive Device.* This is essentially a contact-making ammeter, and since this type of control is more likely to be for an ON–OFF than for a regulating function, it usually is furnished in a single-step control. The standard range of adjustment is 25 to 100 per cent of the coil rating (five amperes).

(*c*). *Kilovar-Sensitive Device.* A kilovar-sensitive device may be thought of as a contact-making wattmeter connected to read kilovars; usually it is an induction-type directional relay for single-phase indication. It is customary to use two master elements for multistep control in order to provide a simple and convenient means for adjusting the band width. One master element can be used for single-step control, and a suitable band width can be obtained by a recalibrating device.

The characteristics of the kilovar or reactive-ampere master elements commonly are given in terms of reactive amperes referred to the secondary side of the current transformer (five amperes normal). A useful range of adjustment is 3.3 amperes (66 per cent) lagging to 1.6 amperes (32 per cent) leading, with a band width adjustable from 2.5 to 0.7 amperes (50 to 14 per cent).

(*d*). *Power-Factor Device.* Since power factor is the measure of an angle, the usual power-factor or angle-measuring devices are suitable for this purpose. What has been said regarding the kilovar-sensitive master element also applies generally to a power-factor-sensitive master element. It is usually an induction type directional relay.

Since the band width in *kilovars* of a power-factor control is variable and changes uniformly with kilowatt load, and because at light load the band width may be smaller than the change in signal caused by switching the largest block of capacitors, some form of desensitizing control is necessary to prevent a pumping action at light loads.

(*e*). *Time-Switch Device.* This is a simple control device used for switching capacitors at a predetermined time, and is used on some utility feeder circuits where the load characteristics with time are reasonably constant.

TIME-DELAY DEVICE

A time-delay device may consist of an electronic or motor-driven timer. Usually, only one time-delay device is used, regardless of the number of steps. If, however, a different time-delay is required for the two sequences, two devices are required. The amount of time-delay is not critical, and is set long enough to avoid unnecessary operation because of momentary circuit disturbances. Range of adjustment is desirable; accuracy in timing is not of extreme importance. Practical experience has indicated that the range of adjustment is from 5 to 20 seconds.

SEQUENCING DEVICE

A sequencing device is used only for multistep control, and consists of an arrangement of interlocked relays or a motor-driven cam switch.

The interlocked-relay scheme is simple, and can be used for two-step control. For a larger number of steps, the motor-driven sequencing switch is simpler and less expensive, and also has the advantage that additional steps of control may be obtained conveniently by adding additional contact positions on the switch. With the interlocked-relay scheme, a major change in wiring and arrangement is required to increase the number of steps.

General Application of Automatic Control Equipment*

FOR REMOVAL OF CAPACITORS AT LIGHT LOADS TO REDUCE VOLTAGE AND LOSSES

In many cases this will be the only switching requirement, and it may be met generally by a single-step voltage-sensitive control that disconnects the capacitor bank when the voltage rises above a certain value, and reconnects it when the voltage falls below a predetermined value.

If the supply-system voltage is subject to variations independent of variations in the load with which the capacitor bank is associated, the voltage-sensitive control may cause the bank to be switched somewhat independently of the load. For example, in some cases the capacitors may be switched off during peak load, or, switched on during light load, with resultant feeder overload or undue losses. This can be obviated by the use of a kilovar or, in some cases, a current-sensitive control.

The inherent characteristic of the

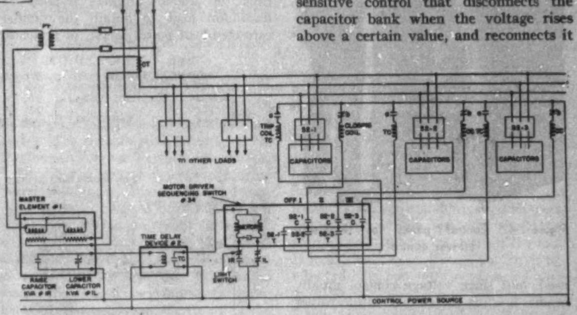

Figure 3. Simplified schematic connection diagram showing three-step kilovar control

Capacitor Switching Sequence (Device 34)
Raise Capacitor Kilovolt-Amperes →

Off	I	II	III
Trip 25-1			
	Close 52-1 Trip 52-2		
		Close 52-2 Trip 52-3	
			Close 52-3

← Lower Capacitor Kilovolt-Amperes

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* A discussion of the application suggestions summarized in Table I.

power-factor-sensitive control requiring it to be desensitized at light load to prevent hunting precludes its use for the application considered in this section.

For Voltage Control

If voltage control is the primary objective, a voltage-sensitive control will be used, and since voltage control usually implies holding the voltage within rather close limits, a multistep control should be used. The capacitor kilovolt-amperes per step should be co-ordinated properly with the voltage band width.

Figure 4. Control panel for three-step kilovar control

For Kilovar or Power-Factor Control*

This application is concerned with bulk kilovar control in connection with kilowatt flow past a purchase or interchange power point, or with maintaining a high power factor to minimize kilowatt losses. The master elements to be considered are kilovar or power-factor-sensitive devices.

In power-dispatching work, the two-quantity concept, which treats kilowatts and kilovars as two separate and independent quantities, is gaining increasingly greater favor. In instances where this concept is adhered to, switching the capacitors as a function of kilovars doubtless will be the predominant approach.

To compare the relative merits of a kilovar-sensitive and a power-factor-sensitive master element where the prime objective is to control the flow of kilovars to a large load, refer to Figure 5. Part (a) shows the circuit involved, with a multistep capacitor bank installed near the load for supplying the load kilovars to maintain a peak load power factor of 95 per cent.

The total kilovolt-amperes of capacitors needed is dictated by the maximum requirements of the load. The number and size of steps in which these capacitors are switched is governed by the fineness of control desired. Unless the kilovar versus the kilowatt component of load (the line *a–b* of Figures 5b and 5c) is known to be fairly constant at all times, it probably will be best to switch the capacitors in uniform steps to prevent the possibility of hunting.

Let the line *o–a* represent the minimum uncorrected load and *o–b* the maximum uncorrected load. Assume line *a–b* to represent the uncorrected-load variation between these two extremes. The line *b–c* then represents the total capacitor kilovolt-amperes applied at maximum load to obtain the desired corrected-load power factor, in this case 95 per cent lagging. The corrected-load characteristics are on the basis of four equal blocks of capacitors.

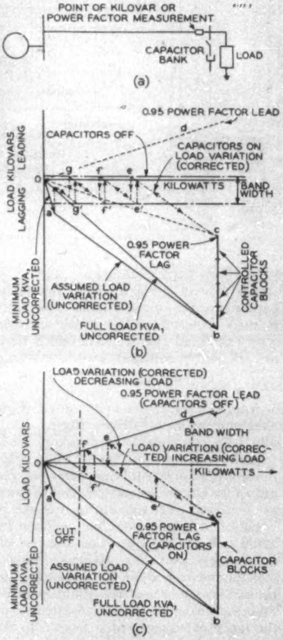
POINT OF KILOVAR OR
POWER FACTOR MEASUREMENT
CAPACITOR BANK
LOAD
(a)
0.95 POWER FACTOR LEAD
LOAD KILOVARS
LEADING
LAGGING
CAPACITORS OFF
d
CAPACITORS ON
LOAD VARIATION
(CORRECTED)
O
g
f
e
KILOWATTS
BAND WIDTH
a
g'
f'
e'
c
0.95 POWER FACTOR LAG
CONTROLLED CAPACITOR BLOCKS
MINIMUM LOAD KVA, UNCORRECTED
ASSUMED LOAD VARIATION (UNCORRECTED)
FULL LOAD KVA, UNCORRECTED
b
(b)
LOAD VARIATION (CORRECTED) DECREASING LOAD
0.95 POWER FACTOR LEAD (CAPACITORS OFF)
d
BAND WIDTH
e
f
LOAD VARIATION (CORRECTED) INCREASING LOAD
LOAD KILOVARS
O
KILOWATTS →
f'
a
e'
c
CUT OFF
0.95 POWER FACTOR LAG (CAPACITORS ON)
CAPACITOR BLOCKS
MINIMUM LOAD KVA, UNCORRECTED
ASSUMED LOAD VARIATION (UNCORRECTED)
FULL LOAD KVA, UNCORRECTED
b
(c)

Figure 5. Comparison of multistep kilovar- and power-factor-sensitive control used to switch capacitors automatically for regulating kilovars

(a). One-line circuit diagram
(b). Performance diagram with kilovar control
(c). Performance diagram with power-factor control

Since the kilovar band width of a power-factor-sensitive control is variable from zero at no load to maximum at full load, it is necessary that the automatic control be made inoperative for loads below which the band width in kilovars is smaller than the capacitor block to be switched. This point is designated by the line labelled "cut off" in Figure 5c. At loads below this, the remaining capacitor blocks are switched manually.

Setting the kilovar-sensitive control in this manner permits the power factor of the corrected load to fall outside the 95 per cent band at light loads; but this is not important in this case, since the basic requirement is to control kilovars. However, the load at which the power factor falls outside the 95 per cent band may be reduced considerably by raising the operating band of the control vertically and sacrificing the very low leading kilovar characteristic. This load can be reduced still further by use of smaller switched blocks.

It will be noted from the uncorrected-load characteristic and the size of capacitor blocks shown in Figure 5c that, if a power-factor control is used, the automatic switching will be done on only two steps instead of the desired four steps as shown. If, for this reason, switching in fewer and larger steps is considered, the "cut off" point will occur at a higher load; this will mean a smaller operating region in which the automatic control is effective.

It can be seen from a comparison of Figures 5b and 5c that a kilovar-sensitive control set for low lagging kilovars and very low leading kilovars will provide a more constant value of kilovars at all loads than does the power-factor-sensitive control. The low leading kilovar feature may be of particular value in this case if it is not desirable to have kilovars flow from load to the power source. A power-factor control entails complications if power flow may be in either direction past the control point.

For Meeting the Requirements of the Power-Factor Clause of an Electric-Power Rate

If capacitors are applied for rate purposes only, and voltage rise and losses at light load are not a problem, it is not necessary to switch the capacitors unless there is a penalty for leading power factor.

If load power factor is determined on an average monthly basis by continuous measurement or by an integration of the kilowatts and the kilovars for the month, it will be advantageous to the consumer to leave his capacitors connected at all times if possible. Otherwise, generally speaking, they can be off except during the period of maximum demand, or during

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* **Exclusive of that for rate-clause purposes.**

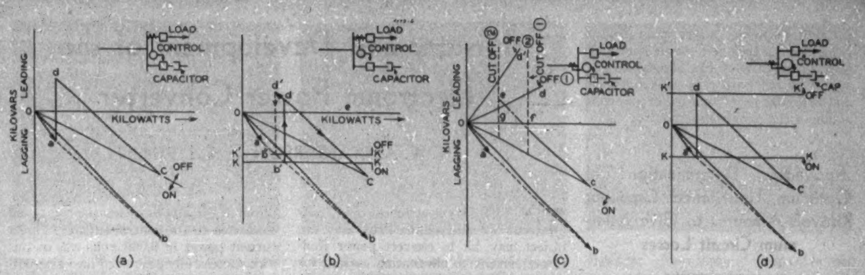

Figure 6. Comparison of single-step kilovar- and power-factor-sensitive control used to switch capacitors applied on behalf of rates

(a). Single-step power-factor control measuring uncorrected load

(b). Single-step kilovar control measuring uncorrected load

(c). Single-step power-factor control measuring corrected load

(d). Single-step kilovar control measuring corrected load

a period of relatively heavy load (not necessarily maximum demand) when the "occasional" measurement is apt to be made.

Where it is desired to have the capacitors on at heavy loads for rate reasons, but off at light load to minimize voltage rise or losses, the kilovar or power-factor master element will provide the most direct solution of the control problem. Unless multistep switching and control to regulate voltage or to keep the losses at a minimum can be justified, a single-step control, judiciously set, will be sufficient.

Figure 6 shows a comparison of the single-step kilovar and power-factor control used primarily in conjunction with a rate clause. The diagrams plot uncorrected and corrected kilovars versus load kilowatts. In all cases a light-load kilovolt-ampere *o–a* and a maximum-load kilovolt-ampere *o–b* is considered, with a straight-line variation between these limits for simplicity. Assume it is desired to hold a power factor at peak load as represented by the line *o–c*. A single-step power-factor control viewing the *uncorrected-load power factor*, as in Figure 6a, switches the capacitors on for load power factors below *o–c* and switches them off for load power factors above *o–c*. (Actually, to avoid unnecessary operation, this type of control would have a band width of a few degrees, but this is neglected in this analysis.) For the load pattern shown, the capacitors are on even at light load, with a resultant leading kilovar component corresponding to point *d*. As the locus of the uncorrected-load kilovolt-amperes proceeds along the assumed pattern *a–b*, the locus of the corrected pattern follows *d–c*. Therefore, for this load pattern, the capacitors are never off and the power factor is unnecessarily high at light load.

If the power-factor control measures the *corrected power factor* as shown in Figure 6c, the ON and OFF characteristics would result in a band width varying with the kilowatt load. However, an auxiliary control device operable at point *f* is necessary to prevent the possibility of hunting. The load range of operation of the control could be increased somewhat by moving the OFF characteristic to *d'*, but this scheme has the disadvantage of the complication interposed by the desensitizing relay and its adjustment.

The kilovar control element measuring *uncorrected kilovars*, Figure 6b, has an operating characteristic *kk* and *k'k'* (OFF and ON again differing practically by a small band width). The relay pickup is set somewhat above the corrected kilovar component of the expected peak load to assure the capacitors being on at heavy loads which are not necessarily up to the expected peak. As the uncorrected load increases, the capacitors go on at *b'*, making the corrected load *o–d*. At peak load the corrected kilovolt-amperes are *o–c* and, as the uncorrected load decreases to *o–b''*, the capacitors are switched off.

If the single-step kilovar control is connected to view *corrected kilovars*, the performance is as shown in Figure 6d. Note that the band width must be widened to exceed slightly the capacitor-bank kilovolt-amperes.

It has been pointed out previously that if multistep switching is used for rate purposes, it probably will be for voltage regulation and to minimize losses. With this objective, it is considered that multistep kilovar control will provide the best results more simply than a power-factor-sensitive control as may be appreciated from a study of Figure 5.

Factors Influencing Operation of Control Equipment

SIZE AND NUMBER OF CAPACITOR GROUPS

The type of automatic control for capacitor switching is independent of the kilovolt-amperes and voltage rating of capacitor blocks. The rating of a block and the number of blocks in a bank is determined by load requirements and the fineness of control desired. It usually is best to switch in blocks of equal rating, unless the daily kilowatt-versus-kilovar pattern of the load is definitely known and known to be the same from day to day.

POINT OF SIGNAL MEASUREMENT

Voltage Control. When automatic capacitor switching is done in response to voltage, the master-control element should obtain its signal from the point at which voltage is to be controlled. Where capacitors are located on feeders supplied from a common bus, with the bus in turn supplied from a system of limited capacity such that switching the feeder capacitors causes appreciable voltage change at the bus, the signal for switching should be taken from the bus. If individual feeder voltage control is attempted, the bus-voltage variation as a result of switching one of the feeder capacitors may require the switching of other feeder capacitors, with a resultant hunting action of the controls.

Current Control. The single-step current control may measure the uncorrected-load current for simplicity. The multiple-step current control should receive its signal from the uncorrected-load current to prevent false operation, and this requires a separate master element for each step, or a recalibration for each step if only one master element is used.

Kilovar or Power-Factor Control. Individual cases will determine whether corrected or uncorrected load should be used as a signal for single-step control (see Figure 6). A multistep control should measure the corrected load to assure proper operation, regardless of possible variation in load characteristics.

DESENSITIZING REQUIREMENT OF POWER-FACTOR CONTROL

It has been shown previously that in most cases the power factor type of control should be made inoperative at low load to prevent the possibility of hunting action. To preclude all possibility of such hunting, the "cut off" or desensitizing operation should occur at some predetermined value of kilowatts; thus the need for a contact-making wattmeter in addition to the power-factor control is indicated. In some cases it is possible that a current relay can be used to provide "cut off" with reasonable accuracy and little danger of hunting or pumping action of the power-factor control.

Appendix. Determination of Optimum Unswitched Capacitor Kilovolt-Amperes to Give Minimum Circuit Losses

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Figure 7 shows the variable portion of the daily kilovar load pattern of a circuit. Let I_c represent the unswitched capacitor kilovars or current. The square of the rms corrected current in the circuit is given by

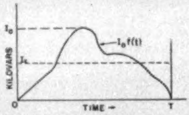

Figure 7. Daily kilovar load pattern of a circuit

$$(I_{rms})^2=\frac{1}{T}\int_0^T \{I_0f(t)-I_c\}^2dt=$$
$$I_c^2+\frac{1}{T}\int_0^T \{I_0f(t)\}^2dt-\frac{2}{T}I_cI_0\int_0^T f(t)dt \quad (1)$$

Differentiate equation 1 with respect to I_c, equate to zero, and solve for value of I_c to minimize the loss, and obtain

$$\frac{d(I_{rms})^2}{dI_c}=2I_0-\frac{2I_0}{T}\int^T f(t)dt=0 \quad (2)$$

or

$$I_c=\frac{I_0}{T}\int_0^T f(t)dt \quad (3)$$

$I_0\int_0^T f(t)dt=$ area of the load pattern, and when divided by T minus the width, the average ordinate is obtained. Hence, for *any* kilovar pattern, the minimum circuit loss with a fixed kilovar supply is obtained by using a value of I_c equal to the *average* value of the requirement.

Reference

1. THE EFFECT OF KILOVAR SUPPLY ON THE DESIGN OF SYSTEMS FOR LOAD GROWTH, T. W. Schroeder, J. W. Butler, N. H. Meyers. ELECTRICAL ENGINEERING, volume 63, February 1944, pages 69–76.

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History and Development of the Electronic Power Converter

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THE TERM "electronic power converter" needs some definition. The object may be to convert power from direct current to alternating current for d-c power transmission, or to convert power from one frequency into another, or to serve as a commutator for operating an a-c motor at variable speed, or for transforming high-voltage direct current into low-voltage direct current. Other objectives may be mentioned. It is thus evidently not the objective but the means which characterizes the electronic power converter. Other names have been used tentatively but have not been accepted. The emphasis is on electronic means and the term is limited to conversion of power as distinguished from electric energy for purposes of communication. Thus the name is a definition.

History

Within the scope of the definition a mercury-arc rectifier is an electronic power converter. From this point of view we must trace its history back more than 40 years. In the first 20 years of this period the development was rather slow. An important discovery however was made by Langmuir in 1913 when he found that a negative electric potential introduced between the cathode and anode prevented the start of current flow through the anode.

At this time the initial development took place of the high-vacuum radio tube which was an evolution of the deForest audion. These tubes were soon used successfully at high voltages to convert direct current into alternating current of audio and radio frequencies and serious thought was given to their use for power conversion. The high internal voltage drop was however a serious obstacle. Attention therefore returned to the mercury arc and ten years after Langmuir's discovery, tests were made which proved that the mercury-arc rectifier could be inverted so as to convert direct current into alternating current. The tests were made with a standard high-voltage rectifier of the type used for series arc lighting. It had a bulbous head and two upturned side arms. Carbon electrodes were introduced at the elbows of the side arms and connected to the control circuit. Direct current power of 9,000 volts was in this way successfully converted into alternating current and then again into low-voltage direct current. A new electrical industry was immediately foreseen where power would be transmitted long distances by high-voltage direct current and railroads would be electrified by locomotives carrying electronic converters from a high-voltage d-c trolley to low-voltage d-c motors. After another 20 years this new industry is now well under way on a modest scale but comprising installations up to 20,000 kw.

Evolution of the Electronic Converter

The first tests of the inverter were made with a mercury-pool tube and the latest installations have mercury-pool cathodes. Between these points there is, however, a significant development. The structure of the tube itself can assume a number of successful forms, such as the thyratron, the ignitron, and the excitron. But all these forms of tubes have certain characteristics in common which force the electrical engineer to accept a new point of view in the methods of its application. This has perhaps been the reason why the practical development has up to the present been rather slow. There has been an unwillingness to face the facts or what so far appear to be the facts, that high-voltage tubes are subject to momentary faults known as arc backs and loss of grid control. The hope that the tubes could be so improved that these faults could be eliminated has so far been wishful thinking. But a much more constructive point of view has been adopted which accepts the electronic faults as inevitable while measures are taken which render the faults perfectly harmless so long as the incidence of the faults is kept within reason. Installations of d-c power transmission and electronic frequency changers are at present operated on this principle with great success. Electronic

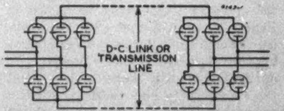

Figure 1. Electronic converter, dual-conversion type

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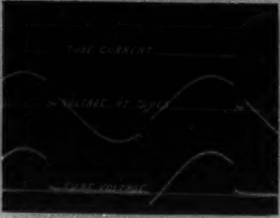


Figure 2a. Wave shapes of single-phase inverter

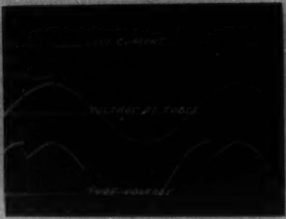


Figure 2b. Wave shape of six-phase inverter with low a-c reactance

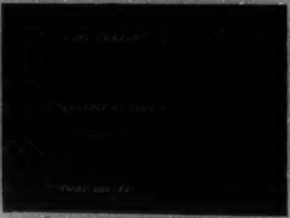


Figure 2c. Wave shape of six-phase inverter with high a-c reactance

faults occur several times a day. They can, however, be observed only by delicate instruments and do not in any way interfere with power service.

Suppression of Electronic Faults

An electronic fault may or may not result in a short circuit, depending upon what means are used to suppress it. In low-voltage rectifier installations the faults occur so seldom that it has proved satisfactory to disconnect the fault by a high-speed circuit breaker. In high-voltage installations the faults occur so often that this would not be permissible because power delivery must be continuous. In the original installation of d-c power transmission for the New York Power and Light Corporation between Mechanicville and Schenectady, this situation was first met by the use of constant current as described in a paper in 1935 by Willis, Bedford, and Elder. In a constant-current system a momentary short circuit is of no significance. Such a system is therefore adapted to the characteristics of the electronic converter. Operation at constant current was successfully continued for four years but when plans were made for future installations it was found that the constant-current system was not well adapted to existing practice of power distribution. The reactance-capacity networks used for conversion between constant current and constant potential were therefore removed and the system has been operating at constant potential for several years. In this connection new means were developed for suppressing the electronic faults. These methods have proved entirely successful and have been adopted in later installations. The sequence of events is illustrated by oscillograms taken during practical operation. Samples of these oscillograms are shown herewith.

Figure 1 shows a simplified diagram of an electronic converter of this type used for d-c transmission and frequency conversion. It should be noted that the power circuits of the rectifier and the inverter are identical and that it is the grid control which makes one part of the whole into a rectifier and the other into an inverter. By change of the grid controls, the power flow can be reversed. The rectifier and the inverter are however not independent devices like a generator and a motor but function together as a whole—the electronic converter. Electronic faults which occur in the inverter are suppressed by the rectifier. The rectifier suppresses its own faults but even this statement must be accepted with some qualifications. The rectifier can suppress its own faults because the inverter cannot feed current back into the rectifier but if several rectifiers feed the same circuit in multiple they can feed current into each other's faults and therefore fault suppression depends upon interaction between the several rectifiers.

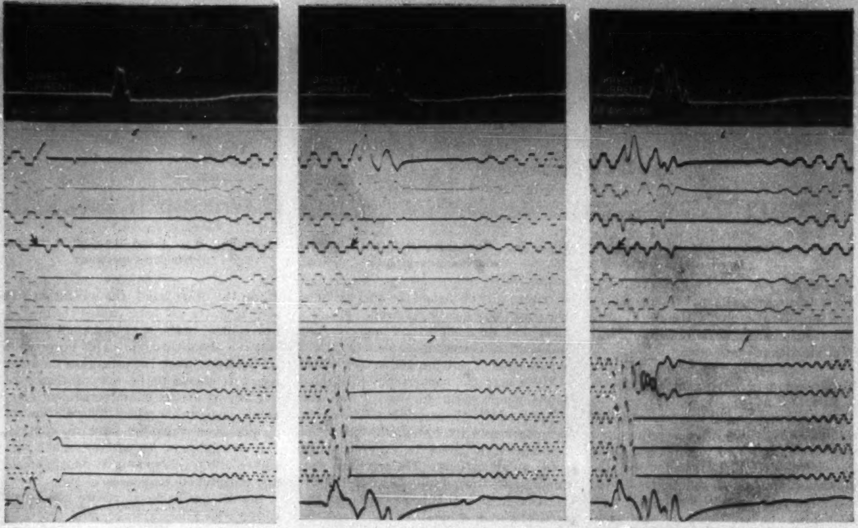
The oscillograms of Figure 2 show a typical performance of a single-phase and a six-phase inverter. The rather complicated wave shapes of the six-phase inverter can be easier understood if the records of the single-phase inverter are examined first. The three recorded curves represent the wave shapes of the current in the a-c line, the voltage between a-c lines, and the voltage between cathode and anode of one tube. The commutation period is marked by the abrupt depression of the alternating voltage near the end of each half cycle. The brief recovery of the voltage before it passes through zero is the margin of commutation. This same margin is also indicated by the brief negative swing of the voltage between cathode and anode. If the load is increased to the point where this margin is used up, the inverter goes into short circuit. With this in mind we can examine the six-phase wave shapes and interpret them as the composite of three single-phase wave shapes. Each phase wave shows its own commutation period and also secondary reactions from the commutation of the other phases.

The electronic fault may be of the nature of an arc back or a loss of grid control known as a "shoot-through." A simple arc-back has so little effect upon the operation of an inverter that it is difficult to discover whether it has taken place. It wipes out the negative voltage that marks the commutation margin but this in itself causes no disturbance. If, on the other hand, the ionization left over from the arc back causes the grid to lose control in the next half cycle the result is a shoot-through. This occurrence is, however, more the exception than the rule. It seems to be one of those sequences of events characteristic of electronic phenomena which follow a law of chance over which we have no control. But as engineers, we are not much disturbed about this so long as we can avoid any harmful consequences.

Besides arc backs there are two other causes which may initiate shoot-throughs in an inverter. These are not electronic faults in the same sense as arc backs, and there is a fair chance that they may be entirely eliminated. One of these causes which has been observed in our earliest installations is voltage transients of very short duration which make the grid momentarily positive at a time when it should be negative. Faults from this cause have been eliminated by the use of suitable filters. A third cause of faults is the occasional failure of an igniter to fire. This difficulty has not yet been entirely overcome but the rate of progress in this technique promises that it will eventually be entirely eliminated.

While thus some causes for faults are gradually being removed we must face the conclusion that shoot-throughs in high-voltage inverters will occur from time to time and we must be prepared to meet this situation in a practical way.

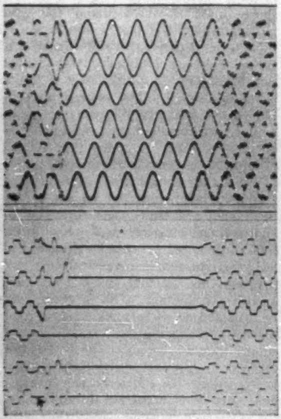
From this starting point there are several possibilities. The inverter may be adjusted so as to work with a good margin of commutation and the power supply may have a short-circuit current that does not greatly exceed the normal load current. In that case the inverter may have enough margin to commutate the increased current and the fault clears itself. It is then difficult to discover that it has occurred and the operator would seldom notice it. Faults which clear themselves are characteristic of circuits containing several rectifier and inverter units in series. It was the normal performance of a 24-phase developmental set tested a number of years ago with four rectifier units and four inverter units in series. This is worthy of note



A. Fault involving opposite inverter tubes in both inverters. Both coils of d-c reactor in. Bottom trace is secondary current of current transformer in direct current

B. Fault including two legs of one inverter transformer. Both coils of d-c reactor in. Bottom trace is secondary current of current transformer in direct current

C. Fault including two legs of one inverter transformer and two legs of one rectifier transformer. Both coils of d-c reactor in. Bottom trace is secondary current of current transformer in direct current



D. Wave shapes of phase voltages and phase currents during faults

Figure 3. Oscillograms of inverter faults. Arrows indicate made fault—igniter miss

when we contemplate d-c power transmission at high voltages. Such installations will necessarily have many units in series and they can be so designed that electronic faults will clear themselves. Each unit in such a system operates at constant potential but an electronic fault in one tube does not disturb the sum of electromotive forces in the system enough to interfere with normal operation.

Returning to the sequence of events that may follow an electronic fault we may assume that in some cases the current which follows the fault is so high that the inverter cannot commutate it. This is the case in the d-c power transmission from Mechanicville to Schenectady and in the frequency changer described in companion papers. In these cases it is necessary to clear the fault by control of the rectifier. The oscillograms of Figure 3 show such a typical sequence. The sudden rise in current acts upon a fast relay controlling the igniter and grid circuit. Consequently the current which has been built up in the power circuit is no longer maintained by any electromotive force and decays at a rate dependent on the time constant of the circuit. It is important that the rectifier should not go into action again until the current has decayed to a value which the inverter can commutate. A time relay delays the action accordingly. In about one-fifth of a second the normal operation is resumed.

The Single-Conversion Frequency Changer

Another type of electronic converter is shown in Figure 4. It is the frequency changer used with the thyratron motor described in a paper by Alexanderson and Mittag in 1935. This electronic converter has not yet been given a name by which it can be readily identified. It is proposed here that it be known as the single-conversion frequency changer. The energy is converted by electronic means only once whereas in the dual-conversion type of frequency changer the power is first converted into direct current and then back to alternating current. Both function in the same way as a nonsynchronous tie between two a-c systems. Besides this the single-conversion type has many desirable characteristics, such as being adapted to feed a stub-end load. The motor for which it was originally developed is a stub-end load and starts from standstill with a good torque like a d-c motor. The single motor may be replaced by a synchronous condenser and an independent distribution system including a multiplicity of motors.

The problem of suppressing electronic faults is solved in this type of electronic converter in a different way. When for one reason or another a loss of grid control occurs in one of the tubes the resulting disturbance lasts only one-half cycle because each tube is a rectifier as well as an inverter and the rectifier function commutates the fault current out of the tube where the fault occurred. The practical result is that this type of electronic converter is entirely free from the problem known as shoot-through in the simple inverter. Thus it will continue to function with slightly reduced power even if several tubes are disconnected, whereas the simple inverter becomes inoperative if a single tube fails to function.

The D-C Transformer

One of the first objectives in the development of the inverter was to provide a way for transforming direct current from high voltage to low voltage. D-c transformation has not yet found application in any large scale. But the experience gained in d-c power transmission indicates that it is from a technical point of view entirely practical. The electronic industry is waiting for a demand where this experience can be applied. The questions that are likely to be asked are: How will it start? Will it require moving machinery? How will it ride through faults?

In order to give a practical answer to these questions, tests have been made represented by the diagram, Figure 5, and oscillograms, Figure 6. The power source is assumed to be a rectifier station like the one used in the d-c transmission from Mechanicville to Schenectady. The receiving station has no moving parts. It has a step-down transformer from the high-voltage inverter to the low-voltage rectifier. A capacitor is in shunt with the transformer. The load is a d-c motor. One oscillogram shows how the operation is initiated from a dead start by application of the grid control to the high-voltage inverter. A second oscillogram shows the characteristic wave shapes.

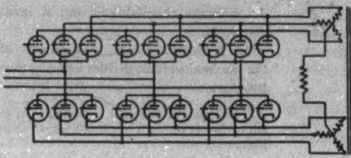


Figure 4 (left). Single-conversion-type frequency changer

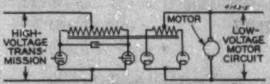


Figure 5 (below). D-c transformer

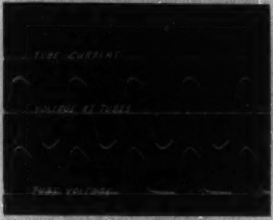


Figure 6a. Wave shapes of inverter in d-c transformer

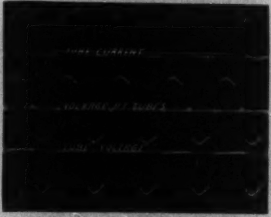


Figure 6b. Wave shapes of rectifier in d-c transformer

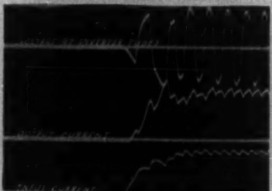


Figure 6c. D-c transformer starting a motor load

Adaptability of the Electronic Converter

Three types of electronic converters have so far been discussed and it has been shown how means have been provided for suppressing electronic faults. In practical operation there are also apt to be circuit faults. The d-c transmission line is for instance sometimes struck by lightning. The short circuit following a lightning stroke is cleared by the control system which is provided for suppressing electronic faults. The transmission line is therefore immune to lightning strokes so long as permanent damage is not done to the insulators. In the design of the electronic converter a virtue has thus been made of a necessity and a real improvement in practical reliability has resulted. Disturbances from other causes than lightning may result in circuit faults. Whether such faults can be counted on to clear themselves automatically depends upon the characteristics and stability of the systems that are involved. An electronic converter has a definite load limit like an inductance motor or a synchronous motor. In the grid control we have, however, excellent means for regulating the system so that those load limits are never exceeded. Such regulating systems have been incorporated in the installations now in operation as described in companion papers.

It is important that the application engineer should understand the characteristics and limitations of the several types of electronic converter.

The rectifier–inverter combination may be described as a frequency changer or a d-c power transmission depending upon whether they are located in the same station or far apart and joined by a d-c transmission line. The Mechanicville-Schenectady installation is a frequency changer as well as a transmission because the power is generated at 40 cycles and delivered at 60 cycles. Similar installations may fill a demand in the future as nonsynchronous links between 60-cycle systems which are not operated in synchronism. This type of installation is primarily adapted to exchange of power between independent systems. In its original form, it is not adapted to start a dead load. When needed, however, suitable arrangements will undoubtedly be made. An electronic d-c transformer fed from a d-c line may be used to start a dead load as shown on the accompanying oscillograms. The power thus made available may be used to start a synchronous condenser so that it can be fed from another electronic converter. This will serve to illustrate many possibilities of using the electronic power converter in one form or another. It can safely be said that with the installations of the electronic power converter already made, this new tool of the electrical industry has come out of its infancy.

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The Evolution of Standard Lines of Current Transformers for High Overcurrent Capacity

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Synopsis: Modern switchgear requires, more and more frequently, special current transformers capable of carrying very high fault currents. In the past, the application engineer and the design engineer were not always close together in their work on the current-transformer problem; the application engineer has often had only an approximate idea of what the design engineer could produce, while the design engineer has had only an approximate idea as to general application requirements. The design engineer has not had too sound a basis for many of his designs. His costs were high on account of the multiplicity of special designs.

This paper discusses the basis of design of current transformers for high overcurrents, and describes the co-ordination of mechanical parts with performance requirements to produce lines of current transformers in a definite and easily understood pattern of characteristics. The result is that selection of the proper current transformer is easily accomplished.

The arrangement of the line of mechanical parts and performance characteristics is such that the expense involved in manufacturing these special transformers can be considerably reduced.

IN the early days of switchgear construction, fault currents were not generally as high as they are today. Special current transformers to carry high currents were rarely needed. Today, however, special current-transformer designs for high current capacity have become common. These "special" designs have taken their place in so much switchgear that they have become in fact standard for switchgear. The basis of their design and rating becomes logically a subject of wide interest and importance.

These current transformers have been most confusing to the application engineer. He has understood that the special transformers for high overcurrent had to have, in general, either higher cost or poorer performance, but there was little generally published information to tell him how high the cost or how poor the performance. He was amazed, in some instances, when he found out. The design engineer, on the other hand, has been confused by the wide variation in performance requirements. The resulting multiplicity of special designs has cost too much to design and to manufacture.

The fundamental basis for solution of the problem is logically a design study, but the evolution of standard lines of transformers to fit all conditions is a most important part of the solution for the application engineer. An application study will be described, together with the method of setting up standard lines of current transformers.

Design Study

The important characteristics of a switchgear current transformer may be listed (given also the insulation class and nominal current ratio)

(*a*). Fault-current capacity[1]
1. Thermal limit (one second).
2. Mechanical limit.

(*b*). Metering performance or accuracy
1. ASA metering accuracy classes.[2]
2. ASA metering burdens.[3]

(*c*). Relaying performance
1. ASA relay accuracy classes.[4]
2. ASA relay burdens.[3]

(*d*). Size and cost.

In general, it is obvious that any characteristic can be somewhat improved at the expense of one of the others. The problem is to get a best compromise of all the characteristics. Let us examine each characteristic separately.

Fault-Current Capacity

1. *Thermal limit.* This limit[1] is defined in a rather complex way which is really equivalent to defining the maximum current density (for one second) as about 90,000 amperes per square inch. To raise the thermal limit, put in more copper.

2. *Mechanical limit.* The study of the mechanical limit divides logically into two parts:

A. Calculation of forces. Various forces act to distort the coils in various ways. (See Figure 1.)

B. Calculation of strength, or the resistance to each form of distortion.

A. Calculation of Forces

The physical nature of the forces in the coils, together with the method used for their solution, is indicated in Figure 1.

Two fundamental principles are available as tools to determine the forces.

(*a*). $$\text{Force} = F \propto Bli \qquad (1)$$

(*b*). $$\text{Force} = F \propto \frac{i^2}{2}\frac{dL}{dx} \qquad (2)$$

The first principle is the more familiar, and is thus more easily used. The second principle can be stated in words, about as follows: the force on an element of a coil, in any direction (taken as x) is proportional to the rate of change of coil inductance with respect to motion of the coil element in that direction. This is a powerful method of attack, since any element or direction of motion may be chosen. The method was demonstrated by Dwight[10] and Clem[5] and used to advantage by Clem in calculating forces in transformers. Both methods are actually used, as expedient.

1. *The expanding force* (which also tends to unwind the outer coil) is most easily calculated according to the assumptions shown in Figure 2. The force

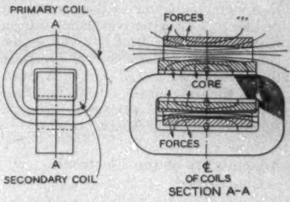

Figure 1. Physical nature of forces

Forces are indicated by the arrows in the right-hand view

In the primary coil, the forces, acting outward, with components toward the vertical centerline tend to expand the outer coil, and to crush it axially

In the secondary coil, the forces tend to crush it against the core

Further inspection of the figure indicates that if the coils are not centered about the centerline of the right-hand view, an axial force tending to slide the coils apart will develop

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formula derivation, also outlined in Figure 2, is very simple. This method obviously introduces some error. However, the expanding force is not the important force if the transformers are constructed as shown in Figure 1, and actual failure is very seldom caused by it. Furthermore, even this approximate method gives more accurate results than the best available formula for axial forces, so that further refinement is without point.

2. *Unwinding force:* The principles of Figure 2 may also be used to calculate the force on the end turns of the coil which tend to unwind the coil. If the coil (Figure 4) is N turns thick, the average field strength through the end turn is $2/N$ of the average value throughout the coil itself. The ampere-turns in the outer turn is $1/N$ of the total ampere-turns in the coil. The force per inch of the end turn of a coil constructed according to Figure 4 is then $(2/N)\times(1/N)$ of the expanding force on the coil as a whole, or $2/N^2$.

3. *Axial-displacement force:* The axial forces have been calculated in a number of ways.[5,6] The method developed by Clem[5] has been found to be most accurate and easier to use. The discrepancy between the methods was found to be such that further investigation into the adequacy of Clem's method was carried out experimentally, as follows:

Experimental Determination of dM/dx

The value of dM/dx or rate of change of mutual inductance with displacement, was carefully measured by measuring M for various axial positions of the inner coil relative to the outer. The change in mutual inductance is very small indeed, but can be adequately measured by comparison with standard precision mutual inductances according to the method outlined by Fortescue.[7] This method can easily be used to measure a change in mutual inductance of 0.01 per cent. The coils used were actual current-transformer coils. Figure 3A shows the coils and gives some of the results of the measurements. The change in inductance at one centimeter displacement is only 1.4 per cent, and as the mutual inductance can be measured to not better than 0.01 per cent the measurement of dM/dx is probably not accurate to better than five per cent.

Experimental Determination of Force

The same coils were used to determine the force, according to the set up in Figure 3B. Again the displacement was accurately measurable to about 0.05 centimeter. The difference between the results of Figures 3A and 3B is about eight per cent. The discrepancy can be attributed to experimental error. The agreement is close enough to show the adequacy of the principle that $F \propto (i^2/2)(dM/dx)$.

Calculation of Force by Clem's Method[5]

The formula is (the notation is from the *original* paper)

$$F=0.88I^2a^2nN10^{6}\times\left[\frac{X_1}{r_1}F_1-\frac{X_2}{r_2}F_2-\frac{X_3}{r_3}F_3+\frac{X_4}{r_4}F_4\right] \quad (3)$$

F = Peak force in pounds

I = Root-mean-square amperes

n, N = Turns in the two coils, per centimeter of axial length

A, a = Effective radii of two coils

$$X_1=X-\frac{S+P}{2} \qquad X_3=X+\frac{S-P}{2}$$

$$X_2=X-\frac{S-P}{2} \qquad X_4=X+\frac{S+P}{2}$$

$$r_1=\sqrt{A^2+X_1^2} \qquad r_3=\sqrt{A^2+X_3^2}$$

$$r_2=\sqrt{A^2+X_2^2} \qquad r_4=\sqrt{A^2+X_4^2}$$

Where X = displacement

$$F_1=1+\left(\frac{a}{r_1}\right)^2\frac{3}{8}\left(\frac{A}{r_1}\right)^2+\left(\frac{a}{r_1}\right)^4\frac{5}{64}\left(\frac{A}{r_1}\right)^2\times\left[7\left(\frac{A}{r_1}\right)^2-4\right]+\left(\frac{a}{r_1}\right)^6\frac{55}{1{,}024}\left(\frac{A}{r_1}\right)^2\times\left[33\left(\frac{A}{r_1}\right)^4-36\left(\frac{A}{r_1}\right)^2+8\right], \text{ etc.} \quad (4)$$

F_2, F_3, and F_4 are similar expressions, functions of r_2, r_3, and r_4 instead of r_1.

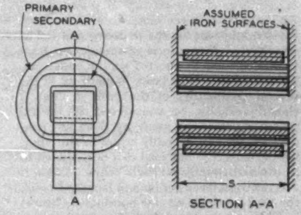

Figure 2. The expanding force can easily be calculated by $F \propto Bli$, if the flux lines are assumed to be straight lines limited at the ends of the coils by magnetic surfaces

The figure shows the lines passing straight across. $B=\dfrac{0.4\pi NI}{S}$

The force is:

$$F=\frac{11.3(NI)^2}{10^7S}$$

peak pounds per inch of coil perimeter when NI = root-mean-square symmetrical ampere turns is fully offset

S = inches length of leakage path

The numerical work in an actual problem is considerable, even with the aid of curves and tables, and it is sometimes difficult to get a true answer, because the answer is a small difference between large numbers, each one the result of considerable calculation. Much simplification is possible by the assumption that $S=P$. This is usually true in power transformers, and nearly true enough in current transformers to give satisfactory results.
Then

$$X_2=X=X_3$$

also, for small values of X,

$$r_2=r_3=A$$

and

$$F_2=F_3$$

$$\frac{X_2}{r_2}F_2=\frac{X_3}{r_3}F_3$$

$$X_1\cong-\frac{S+P}{2}\cong-X_4 \text{ and } r_1\cong r_4$$

The corresponding values of a/r and A/r will usually be so small that

$$F_1\cong F_4\cong 1.0$$

Hence

$$\frac{X_1}{r_1}F_1+\frac{X_4}{r_4}F_4=0$$

Returning to equation 3

$$\text{Force}=1.776I^2a^2Nn\frac{X}{r_2}F_2 10^{-6} \quad (5)$$

or

$$\text{Force}=1.776(N_SI_S)^2\left(\frac{a}{S}\right)^2\frac{X}{A}F_2 10^{-6} \quad (6)$$

where N_SI_S = ampere turns (root-mean-square) in coil of axial length S. F_2 is taken from the table in Clem's paper. This formula is obviously much easier to use than the original (equation 3).

Using this formula, and assuming that round coils equivalent to the actual coils in Figure 3 are those which have the same area inside the mean turn, the force comes out to be

$$\text{Force}=0.0614I^2X$$

where X is in centimeters. This compares favorably with the actual value of force by either of the other methods (Figure 3)

From measured value of $\dfrac{dM}{dx}$, $F=0.0867I^2X$ grams; 108 per cent

From measured value of force, $F=0.08I^2X$ grams; 100 per cent

From Clem's method simplified, $F=0.0614I^2X$ grams; 77 per cent

Taking the measured value as correct, the force on the actual transformer coil

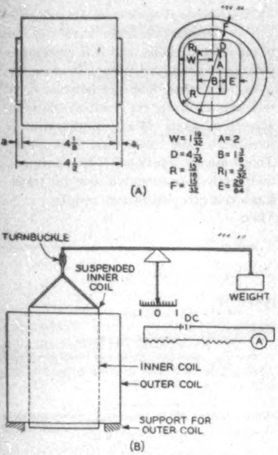

Figure 3. Coils used in experimental determination of *dm/dx* and *F*

A—The relative axial position was determined from measurements of a and a_1. The center position ($a=a_1$) did not give quite the maximum value of M. The point at which M was actually maximum was taken as center. If X is displacement from this center (centimeters), the values of M followed very closely the relation:

$$M=0.00031731-0.00000425X^2$$

from which $dm/dx=0.0000085X$ and force $=0.0867XI^2$ (grams)

B—Procedure of measurement:

1. Adjust position and weight so that balance is attained *with* or *without* current in the coils. This will be when the coils are electrically centered.

2. *Without* current determine displacement (1.14 centimeters) as additional weight (1 gram) is added.

3. *With* current (7.8 amperes direct current) determine weight (6.55 grams) to produce same deflection.

4. Force due to current is 5.55 grams. $F=0.08\times I^2X$ (compares with $0.0867\ I^2X$ from Figure 3A).

at normal current ($10\sqrt{2}$ peak amperes) and 0.1 inch (0.254 centimeter) displacement would be

$$F=0.08\times2\times100\times0.254=4 \text{ grams}$$

At 200 times normal current the force would be $200^2\times4=160{,}000$ grams = 352 pounds. This compares favorably with the force of 600 pounds required to separate the coils, described later.

This method is obviously not too exact. However, it is never possible to know the possible displacement in an actual transformer with great accuracy. It is necessary to make some assumption as to maximum possible displacement, which must be assumed large enough to be on the safe side. This automatically provides a fairly large factor of safety, so that great accuracy of calculation is unnecessary. The method given will give a sufficiently accurate comparison of forces in various designs, which is all that is required for the purpose.

B. Calculation of Strength

The resistance to each force can be calculated as follows:

1. *Expanding force.* The expanding force in a coil appears as a tension in the copper which can be calculated in the same way as the tension in the walls of a cylinder subjected to internal hydraulic pressure.

Given:

F = force per unit of circumference
d = diameter of the coil

Tension in one side $=Fd/2$ and the stress in the copper is $Fd/2$ divided by the total copper cross section. In the coils in question this tensile stress (see Figure 2) is never the critical one; failure always occurs by axial displacement.

2. *Unwinding force.* The end turn is held down in three ways:

By the Micarta terminal brace (Figure 4).
By the treated tape insulation wound tightly over the coil.
By the stiffness of the copper.

Of these, the first and third are difficult to calculate, although they are considerable. The strength of the treated tape is easily calculated, however, and is found to be sufficient to hold the end turn in place, assuming an ultimate tensile strength of 5,000 pounds per square inch for the tape. (For 0.010×1-inch tape, a breaking strength of 50 pounds.)

3. *Arching of straight side of primary coil.* The expanding force tends to arch, or bow, the straight side of the primary coil. The strength of the side is obviously at least that of a beam supported at the ends, uniformly loaded. The beam is made up of the several conductors of the coil, so that it should actually be considered as several separate beams, as many as there are conductors, which is usually equal to the number of turns N. The load can be assumed to be divided equally on each beam. Then, if F is the total force per inch of length of coil l, the load per inch on one conductor (beam) is F/N and the maximum moment is

$$\text{Moment}=\frac{Fl^2}{N8}$$

and the maximum stress is the familiar MC/I

$$\text{Stress}=\frac{\text{moment}\times{}^1/_2\text{ conductor thickness}}{\text{moment of inertia of one conductor}}$$

$$\text{Stress}=F\times\frac{l^2}{8}\,\frac{\dfrac{\text{conductor thickness}}{2}}{\dfrac{1}{12}\text{ conductor width}\times\text{conductor thickness}^3}$$

The stress will usually come out to be about 50,000 pounds per square inch at the current at which the limiting axial force is reached. The inference is that the coil will fail by bowing out of the straight side. Actually, however, two important factors enter to reinforce the coil at this point:

1. As soon as a very slight bow has occurred, the beam is much stronger.

2. The core, backed up by a heavy steel base, takes much of the stress as soon as a slight deflection occurs.

The actual strength is very difficult to calculate. Tests to be described, however, show that this stress is not the limiting one.

4. *Resistance against axial movement.* A number of coils were made and the strength measured by actually pressing the inner coil out of the outer coil in a testing machine. The force required for the coil shown in Figure 4 was of the order of 600 pounds to start the coil moving. The cement used is a plastic, and its resistance is viscous in nature. The force required to move the coils very rapidly, as they do when actual failure occurs, is certainly higher, but it is not possible to say how much. From these tests the effective shearing strength of the cement was determined and strengths of other size coils calculated. A high-strength thermosetting cement was found to be practical for those designs made for high thermal capacity in oversize frames, to make the mechanical limit comparable with the high thermal limit. The strength of this cement is more than twice that of the plastic cement. The designs with which this cement is used have oversize copper, so that the tensile strength of the coil is increased about in proportion, and as they have, in general, more than four turns, the unwinding force was not a limit. The mechanical strengths were thus made higher in these designs in about the same ratio as the thermal limits.

Strength Tests

The critical strengths are evidently (1) the axial displacement resistance and (2) the bowing of the straight side of the coil.

The possible initial axial displacement in a coil the next size smaller than Figure 3 was deliberately made 0.16 inch by displacing the last coil layer. According to Clem's method, simplified, the axial stress was then 650 pounds at 100 times normal current, while the resisting force averaged 600 pounds. This transformer withstood 100 times normal current, as a fully offset wave could not be obtained, but at a slightly greater current the secondary coil was violently ejected, taking the core with it. Once the secondary coil moves, the force is increased, and although the current lasted only three cycles, there was ample energy delivered to do considerable damage.

Another unit was very carefully centered and cemented with high-strength cement so that an expanding force of about 8,200 pounds per inch, with a theoretical stress in the straight side, as a beam, of 98,000 pounds per square inch at a current of 350 times normal could be developed. (Force per inch of coil length = 8,200 pounds.) This transformer actually withstood this current without damage. When the current was raised to 470 times normal fully offset, a bowing of the primary coil was produced. (Force per inch of length = 14,700 pounds.) The resulting bow did not destroy the transformer, but produced distortion of the core and frame.

The final justification of the methods consisted of calculating the mechanical limit of a number of designs, and testing them all (over 15 units) at currents exceeding the calculated limits, without damage. Higher currents produced axial movement in some units not perfectly centered, and still higher currents destroyed a few units which were centered, by bowing of the coil.

Table I

150/5 = 8 primary turns to 240 secondary turns
100/5 = 8 primary turns to 160 secondary turns
75/5 = 8 primary turns to 120 secondary turns
50/5 = 8 primary turns to 80 secondary turns
40/5 = 8 primary turns to 64 secondary turns
30/5 = 8 primary turns to 48 secondary turns

The final strength limit is fixed by the axial force on account of the assumption that these coils cannot invariably be initially centered closer than 0.1 inch. This dissymmetry is caused by a number of variations.

1. Dissymmetry due to leads.
2. Misplacing of the last (partial) layer.
3. Dissymmetry of assembly of the coils.

It is possible to control these factors in manufacture, but as it is not practical to make any tests for symmetry of the final assembly, it is considered that it would not be conservative to assume that the initial displacement would be less than 0.1 inch.

Metering and Relay Performance

The method of calculation used has been described previously.[8] The quality of the problem to be solved can be illustrated by pointing out that the errors in transformation vary (other things being equal) approximately as follows:

Errors increase with increasing secondary burden in ohms.

Errors increase with decreasing secondary turns (about as the square).

The mechanical limit (limited by forces) increases directly with decreasing secondary turns.

The thermal limit (considered in times normal) also increases with decreasing secondary turns (again almost directly).

The internal burden, however, decreases with decreasing secondary turns (about as the square).

The relations are not exact but they illustrate the conflict between errors on the one hand and burden and current limit on the other.

Application Study

The design study is of fundamental importance, but the method by which it has been applied is of greater general interest, because it is based on ideas which are new and by which a unique result has been obtained.

Obviously a practically infinite number of current transformer designs would be required to suit all the applications exactly. Therefore, some study must be made of the field of application to allow the fundamental design principles to be applied. A limited number of standard designs is obviously desirable, but it is not clear, at first glance, what to choose as standard. The following paragraphs describe the evolution of standard lines of transformers.

Standard Overcurrent Limits: Correlation With Circuit Breakers

Standard switchgear practice[9] is to specify, for the current transformer, overcurrent capacity equal to that of the circuit breaker with which it is used. The logical conclusion, therefore, is to design a standard line of current transformers to go with each of the commonly used circuit breakers. Four standard lines of current transformers in each insulation class are indicated, designed for fault currents of 14,500, 26,000, 36,000, and 46,000 amperes.

Standard Cores

Four core sizes have been found to be sufficient to cover the range of requirements from 5- to 15-kv insulation classes. More details on the cores are given later.

Standard Primary Coils

Given the insulation class, overcurrent limit, and core size, the primary-coil design is fixed, since the primary turns and copper section are determined by the overcurrent limit. The maximum possible number of primary-coil designs is then the product of 3 (insulation classes 5, 8.7, 15 kv) times 4 (overcurrent limits listed above) times 4 (core sizes) or 48. As shown in Table III, the useful number of designs is only 28. Furthermore, as shown in the same table, 12 of these designs are taken from the standard commercial line of transformers. Finally, then, only 14 special primary coils need to be designed, built, or carried in stock.

Secondary-Coil Design

In making the core designs and the designs for the primary coils, space will have been left for a secondary coil to have substantially the same total copper cross section as that of the primary coil, so that the overcurrent density in the secondary copper will be substantially the same as in the primary, regardless of the number of secondary turns.

Table II. ASA Metering Accuracy Classes Related to Secondary Turns (for Any Core)

Secondary Turns*	Burden 0.1	Burden 0.5	Burden 2.0
240	0.3	0.3	0.3
200	0.3	0.3	0.6
160	0.3	0.3	1.2
120	0.3	0.3	2.4
100	0.3	0.6	2.4
80	0.3	0.6	4.8
60	0.6	1.2	
40	0.6	4.8	
30	1.2	4.8	

* Nominal: The actual value is slightly smaller so that the actual current ratio will come out right.

The various nominal current ratios are obtained with a given primary coil by varying the number of secondary turns. For example, the eight-turn primary coil in core number 1 is used to make the ratios shown in Table I.

As the number of turns is reduced, the copper section in each turn must be increased. Obviously all ratings will carry 150 amperes continuously. The last one, for instance, the 30/5, may accurately be described as 150/25. The design of the secondary coil is obviously very simple.

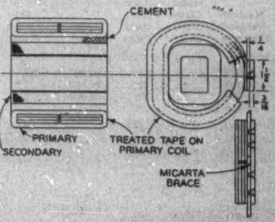

Figure 4. The primary coil shown is that of the 200-ampere rating, wound of heavy copper strip in two sections

The end (outside) turns are held down by the brace which holds the terminals together, and by the treated tape. The secondary and primary are held together by cement

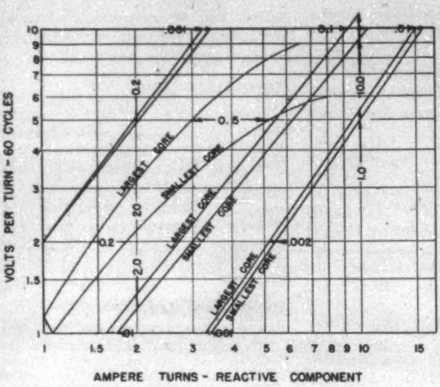

Figure 5. The saturation curves for the cores are practically identical over the lower (metering) range, so that the metering performance of all cores is the same

The curves vary widely over the upper (relaying) range, but as the relay performance rating is a function principally of the saturation voltage, it is not too difficult to work out relay performance ratings for each one

Performance

As the number of secondary turns is reduced, the performance becomes poorer. In a given core, the performance depends only on the number of secondary turns. The performance can be calculated for each number of secondary turns and core size, but the great amount of numerical work creates an impossibly tedious problem. The problem was made practical only by designing the cores as follows:

1. The saturation curves, in the lower (metering) range were made substantially identical (Figure 5).

2. The proportions of the window opening were so adjusted that the internal reactance of the secondary coil comes out the same in any core (for a given number of secondary turns).

This means that the calculation carried out for one core holds for any core, and that the performance for any design can be read from a single simple table, such as Table II.

Final Standard Lines of Transformers (Table III)

Each primary coil in Table III represents a line of transformers covering a rather small range of current ratios, as in Table I, and the performance of each of these ratios can be read from Table II. The total possible number of designs is the product of the number of primary coils times the number of secondary coils used with each primary. The number of ratios in each line is small because the highest ratio is that which has the normal maximum number of turns in the secondary, while the lowest ratio is the one which has the minimum number of secondary turns for usable performance. However, as the table shows 28 lines of transformers, which, with at least six ratios in each line, is 168 new designs, it might seem that a great deal of design work is yet to be done. However, after the 14 special primary coils have been designed, the design of the secondary coils is almost simple clerical work.

The final complete table of transformers would require considerable space and is not included. Each space in Table III is expanded, in the manner of Table I, into a line of transformers. Each line has the one *continuous* primary-current rating in all ratios. The performance characteristics fall into a consistent pattern.

The transformers are constructed in a similar way, and the fact that only four sets of mechanical parts and only 28 primary-coil designs are required reduces manufacturing expense.

The problem of selection is almost completely solved for the application engineer, since he can very quickly find the design he wants in the complete tables furnished by the manufacturer.

Control of quality is much easier. The user of the transformers can be more certain that transformers selected from a standard line of proved performance, will be more satisfactory.

Table III

Mechanical Limit (Amperes)	Insulation Class (Kilovolts)	Core No. 1		Core No. 2		Core No. 3		Core No. 4	
		Primary Turns	Thermal Limit	Primary Turns	Thermal Limit	Primary Turns	Thermal Limit	Primary Turns	Thermal Limit
14,500	5	8*	12,500	12	14,500	14	17,000		
26,000	5	4*	26,000	7	26,000	8	30,000		
36,000	5	3*	36,000	5	36,000	6	50,000		
46,000	5	2*	50,000	3	50,000	4	60,000		
14,500	8.7			8*	12,500	14	14,500		
26,000	8.7			4*	25,000	8	26,000		
36,000	8.7			3*	36,000	6	36,000		
46,000	8.7			2*	46,000	4	50,000		
14,500	15					8*	12,500	12	14,000
26,000	15					4*	25,000	7	27,000
36,000	15					3*	36,000	5	36,000
46,000	15					2*	46,000	4	50,000

* These primary coils are taken without change from the standard commercial lines of transformers; the transformer construction and dimensions are standard. The coils with 8, 4, 3, 2 turns, respectively are the coils for standard 150, 300, 400, and 600 amperes, respectively. The other designs are made with special primary coils. The higher-strength coil cement is used to resist the higher mechanical forces generated by the higher values of ampere turns. These designs are accordingly more expensive, but of course the performance of these designs is much better.

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Totalizing Contents of Aircraft Fuel Tanks

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THE requirement of totalizing two or more liquid levels in aircraft fuel tanks falls into two broad classifications. The first involves the addition of levels where the sequence of use is definitely established. This type is best exemplified by considering a long wing tank where the dihedral angle makes it necessary to have more than one transmitter in the tank to obtain complete coverage of the fuel contents. Figure 1 shows such an arrangement. With the fuel outlet as shown, transmitter 2 must function first. In other words, the sequence of operation is fixed so that the operation of transmitter 2 is always followed by that of transmitter 1 as fuel is withdrawn from the tank.

The second category covers the totalization of several liquid levels in different tanks where the sequence of use is not established, but where single indication of the total fuel is desired. For the three separate tanks each with its transmitter, shown in Figure 2, the sequence of use of the fuel in the three tanks may vary widely. Such variations may be caused by the puncture of one or more tanks by gunfire, by the necessity of trimming the plane, or by other abnormal changes.

Principle of Operation

In order to explain the operation of the totalizing circuit it is desirable to show first the basic principles of a similar circuit when used in the conventional manner. Figure 3 shows a typical liquid-level transmitter now used in aircraft fuel tanks. The cork float at the end of the lever floats on the surface of the fuel. As this surface level changes the motion of the float is transformed through suitable gearing and sealing mechanisms to a change in resistance of a potentiometer unit located in the head of the transmitter.

Figure 4 shows schematically this potentiometer connected to the element of the indicator on the instrument panel. This element has two coils, located 140 degrees apart, one of which is connected directly across the battery through a series resistor. The current through this coil is constant except for changes in the line voltage. The other coil is shunted by the potentiometer in the transmitter. The amount of shunting resistance, and hence the current in the indicator coil, is therefore dependent upon the position of the transmitter float and varies with the liquid level.

The vector diagram of Figure 5 further illustrates the principle of operation. The permanent-magnet rotor to which the indicator pointer is attached aligns itself with the resultant vector, a vector obtained by the addition of the constant-current-coil flux and the variable-current-coil flux. The general design of this indicator is similar to one described in an earlier paper,[1] except it has been made up in a ratio-instrument[2] form for this application.

Sequence Totalizing

In a simple two-transmitter-sequence totalizing circuit the variable-current coil of Figure 4 is double wound. Each section of this double winding is connected to a transmitter as shown schematically in Figure 6. The flux from the variable-current coil is the sum of the fluxes produced by the currents flowing in the two parts of the double-wound coil. The conditions of the system described in Figures 4 and 5 are duplicated. Only one value of resultant flux is possible for any tank content; therefore, the indicator reads the correct fuel quantity. The indicator-scale distribution is established by the shape of the tank and the values of the circuit resistances.

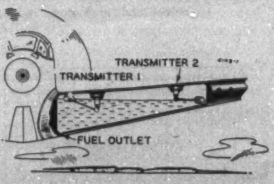

Figure 1. Installation showing necessity of two transmitters to obtain complete fuel coverage

Nonsequence Totalizing

Since the indicator must show correct readings regardless of how the fuel is distributed in the tanks being totalized, the nonsequence-totalizing system presents a different problem from the sequence-totalizing system just described.

As in the case of sequence totalizing, there is a separate winding on the variable coil in the indicator for each transmitter used. In order to make the indication of the total fuel quantity independent of the sequence of consumption, the increment of flux and ampere turns per gallon of fuel associated with each winding of the variable coil must be constant and of the same value for all tanks. The total ampere turns produced by the variable coil can then be represented by the equation:

$$NI = \Delta ni(g_1+g_2+g_3+\ldots) \qquad (1)$$

where NI is the total ampere turns produced by the variable coil, Δni is the increment of ampere turns per gallon, and g_1, g_2, and g_3, are the gallons present in tanks 1, 2, and 3, respectively. In order to obtain the condition shown by this equation consideration must be given to:

1. The shape of the tanks.
2. The dimensions of the transmitters.
3. The electrical circuit.

The simplest tanks to totalize would be those having a straight line as the plot of gallons versus float angle. However, it is impractical to meet this condition and the nearest practical approach is a tank with uniform cross section at all liquid levels. With a tank of this construction it can readily be shown that:

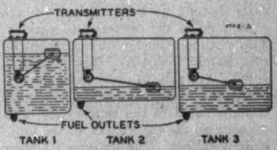

Figure 2. Installations in three different tanks with total fuel in all tanks to be indicated on a single dial

$$x = L(1-\cos\theta) \qquad (2)$$

where x is the depth of the liquid, L is the length of the float lever, and θ is the angle formed by the float lever and a perpendicular to the liquid level. This equation is shown graphically in Figure 7. For uniform-cross-section tanks, the dimensions of the transmitters can be designed so that θ is kept between 60 degrees and 120 degrees to utilize the portion of the curve which is almost a straight line. Although a constant cross section is not always obtainable, it is often possible to vary the transmitter dimensions in order to utilize the curve of Figure 7 in such a manner as to compensate for the shape of the tank. Control over the characteristics of the electrical circuit can be obtained by introducing corrective resistance values R_1, R_2, R_3, and R_4 into the

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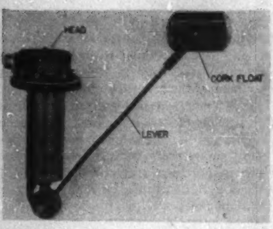
HEAD
CORK FLOAT
LEVER

Figure 3. A typical liquid-level transmitter

proper parts of the circuit as shown in Figure 6. These resistances also provide an additional means of compensating for the nonlinear relation between gallons and float angle which may exist even when the optimum dimensions of the transmitter have been selected.

For example, consider a single section of the dual-wound coil and its accompanying transmitter potentiometer in Figure 6, where R_c represents the variable-coil-circuit resistance; R_{T_1} and R_{T_2} the transmitter potentiometer resistances. R_{T_1} and R_{T_2} always add to give R_T the total potentiometer resistance. The total ampere turns necessary to operate the indicator element are known from its design when used in the conventional manner. The maximum ampere turns are contributed by any one tank when that tank is full. The ratio of these maximum ampere turns to the total maximum ampere turns is equal to the ratio of the tank capacity to the capacity of all the tanks being totalized. The maximum ampere turns occur when g_1, g_2, and g_3 of equation 1 are equal to the capacities of tanks 1, 2, and 3, respectively.

Table I. Variations in Indication With Changes in Use Sequence

Total Fuel, Gallons	Average Indicated Gallons With Seven Various Sequences of Use A	B	C	D	E	F	G
0	0	0	0	0	0	0	0
100	100	110	98	92	94	98	90
200	200	208	206	198	198	192	198
300	300	294	308	298	298	288	288
400	400	386	410	400	400	386	
500	500	500	500	500	500	500	

Sequence of use *A*—As calibrated.

Sequence of use *B*—Tanks 1, 2, and 3 simultaneously.

Sequence of use *C*—Tank 2, tank 1, tank 3.

Sequence of use *D*—Tank 1, tank 2, tank 3.

Sequence of use *E*—Tank 1, tank 3, tank 2.

Sequence of use *F*—60 per cent tank 2, 60 per cent tank 1, 100 per cent tank 3, 40 per cent tank 1, 40 per cent tank 2.

Sequence of use *G*—60 per cent tank 1, 60 per cent tank 3, 60 per cent tank 2.

Tank 1 contents—200 gallons.
Tank 2 contents—100 gallons.
Tank 3 contents—200 gallons.

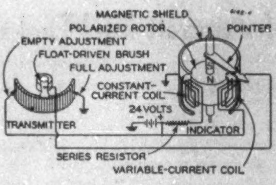
MAGNETIC SHIELD
POLARIZED ROTOR
POINTER
EMPTY ADJUSTMENT
FLOAT-DRIVEN BRUSH
FULL ADJUSTMENT
CONSTANT-CURRENT COIL
24 VOLTS
S
N
TRANSMITTER
INDICATOR
SERIES RESISTOR
VARIABLE-CURRENT COIL

Figure 4. Schematic cut-away view of indicator and transmitter

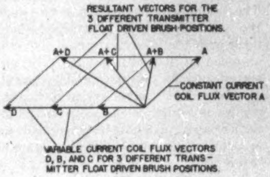
RESULTANT VECTORS FOR THE 3 DIFFERENT TRANSMITTER FLOAT DRIVEN BRUSH POSITIONS.
A+D
A+C
A+B
A
CONSTANT CURRENT COIL FLUX VECTOR A
D
C
B
VARIABLE CURRENT COIL FLUX VECTORS D, B, AND C FOR 3 DIFFERENT TRANSMITTER FLOAT DRIVEN BRUSH POSITIONS.

Figure 5. Vector diagram of fluxes for three currents in the variable coil

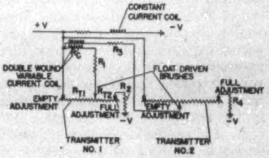
CONSTANT CURRENT COIL
+V
-V
R_C
R_3
R_1
DOUBLE WOUND VARIABLE CURRENT COIL
FLOAT DRIVEN BRUSHES
R_{T1}
R_{T2}
R_2
FULL ADJUSTMENT
R_4
EMPTY ADJUSTMENT
FULL ADJUSTMENT
-V
EMPTY ADJUSTMENT
-V
TRANSMITTER NO. 1
TRANSMITTER NO. 2

Figure 6. Schematic diagram of a two-transmitter totalizing system

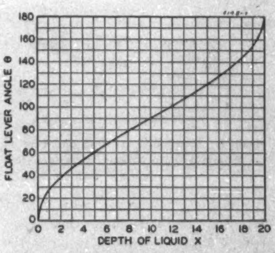

Figure 7. Curve of depth of liquid versus float-lever angle for a tank of constant cross-sectional area

$$x = L(1-\cos\theta);\ L = 10$$

To insure uniformity of positioning of the sections of the multiwound coil, the individual windings are all wound in a single operation keeping the several wires as closely together as possible. Each winding of the coil then has the same number of turns.

To meet the conditions of equation 1 it is necessary to vary R_1, R_T, and R_2 so that the curve of the current in the coil versus gallons in the tank is a straight line. Since in practice this straight line cannot often be attained, the closest possible compromise must be used.

To plot this current-versus-gallon curve, an intermediate curve of the tank calibration must be used. The calibration curve shows the liquid level in the tanks versus gallons. This liquid level in turn is translated into angular movement of the float lever and finally into values of per cent R_T where

$$\text{Per cent } R_T = (R_{T1}/R_T)100 \qquad (3)$$

The amount of current in the variable indicator coil for various liquid levels can then be readily calculated by solving the simple circuits of Figure 6, and the resistance values determined to give the closest approximation to a straight-line relationship between current in the variable coil and gallons in the tank. Similar curves must then be obtained for the other tanks.

When the values of R_1, R_T, and R_2 are chosen for one of the tanks the calculations for the remaining tanks become simple, providing the tanks are the same shape. When this is true the values of R_1, R_T, and R_2 are multiplied by a suitable constant proportional to the contents of the tank being considered in relation to the total contents of the system. If the other tanks are of different shapes, the values of R_1, R_T, and R_2 must be determined for each by the same process used on the first tank, so that for each tank the curve of current versus gallons is a straight line of the same slope.

Application of Nonsequence Totalizing

Figure 8 is a photograph of a totalizing-indicator element used to read the total fuel in three tanks. The variable-current coil located beneath the magnetic shield is triple wound. Manufacturing difficulties in producing this multiwound coil limit the number of windings and hence the number of tanks which can be totalized with a given-size element. Figure 9 shows the complete instrument using the element in Figure 8. The scale

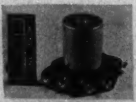

Figure 8. A three-transmitter totalizing indicator element

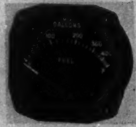

Figure 9. A three-transmitter totalizing indicator using element of Figure 8

distribution is inherently open at the lower end and closed at the upper end. This arrangement gives better instrument readability for the smaller fuel quantities which are the most important.

Since it is impossible to obtain the exact requirements of equation 1, indications of a given fuel quantity vary slightly with the distribution of the fuel in the tanks as determined by the sequence of use. The indicator is calibrated with the fuel distributed in the tanks in the manner determined by the normal or average sequence of use. Table I shows how the indication of 100, 200, 300, 400, and 500 gallons varies with sequence of use for the instrument of Figure 9. The contents of the three tanks being totalized, and seven different sequences of use are shown in the table.

Advantages of Totalizing System

The totalizing systems discussed have several advantages over the series operation of two or more transmitters often used to accomplish the same results. For sequence totalizing one commonly used system employs two or more transmitter potentiometers connected in series so that only one is in the circuit at a time. In order to avoid open circuits at the change-over points, the adjustment and installation of such series-connected transmitters is critical, since the point where one ceases operation and the other starts must be controlled within close limits. Stops are often attached to the transmitter properly to locate these change-over points. These are not necessary with the totalizing method described above since the top and bottom of the tank can be used as stops.

Series connection of the transmitters has been used for nonsequence as well as sequence totalizing. In either case an open circuit of one of the units causes failure of the whole indicating system. In the totalizing method described, however, the rest of the units operate in their normal manner, even when one of the transmitters is removed from the circuit.

Moreover, the series-connection method does not readily permit the adjustment of circuit resistances to compensate for variations in tank shapes, as has been described, and is therefore more limited in application.

Conclusion

To date the application of the totalizing system has been limited to three transmitters per installation. While the use of these methods for a larger number of transmitters and tanks would tend to complicate the equipment and would require somewhat larger indicator elements, such applications are entirely practical. Industrial liquid-level-measuring applications are inferred, in addition to the indication of aircraft fuel supply for which these systems were developed.

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Ring Busses for Generating and Transmission Stations

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IN recent years, the Commonwealth Edison Company has adopted bus layouts for generating stations and transmission terminals which differ greatly from those installed in its older stations. The purpose of this paper is to describe these developments and to discuss the application of the ring-bus principle to high-voltage installations. It appears to be an appropriate time to review the characteristics of various bus layouts in view of possible postwar expansion and rehabilitation of existing stations.

The basic problem in determining the most desirable bus layout is to obtain reliability, flexibility, and adequate relay protection, at a reasonable cost. In the earlier period of development of bus layouts in large metropolitan stations, the most important of the above factors was reliability of supply. It was found that a duplicate power supply for all transmission lines emanating from the generating station was essential for reliable service. However, as methods of station construction improved and advances were made in the development and design of station equipment and bus-protection schemes, the requirements for bus layouts in generating, distributing, and transmission stations have also changed. In addition, operating experience has indicated certain desirable features which should be incorporated in bus layouts. This paper will show how the bus arrangements used in the Commonwealth Edison system have been changing through a period of years to take advantage of improved equipment and experience in the operation of the system.

12-Kv Busses

Description of Bus Layouts

The conventional type of double-bus arrangement, which was used in the Commonwealth Edison Company up to 1936, is illustrated in Figure 1A. In this arrangement, there are two sets of main busses, each set consisting of several sections separated by section tie breakers. The generators, high-voltage transformers, and line busses which feed the outgoing lines are so arranged that they may be connected to either or both sets of main busses. The substations supplied by the outgoing lines have a multiplicity of supply lines so selected that the outage of one line bus does not result in loss of load. The number of lines connected to each line bus has varied; earlier stations have three lines per line bus, while later stations have four lines per line bus.

Several variations in this conventional bus have been used. At one station the two sets of main busses are connected through two reactor ties as shown in Figure 1B. The purpose of these reactors is to reduce short-circuit currents with the two main busses operating in parallel. In this station, some of the generators, transformers, and line busses are operated closed to one of the two main busses, and the remainder to the other main bus.

In another case, reactors were used between the main bus sections as shown on Figure 1C. The advantage of this arrangement over Figure 1B is that it permits parallel operation of the two main busses with opposite bus sections solidly tied, and yet provides a reduction in short-circuit current. However, these reactors create a disadvantageous situation with respect to power flow on the outgoing lines. These lines operate in parallel at the substation end, with the result that any phase-angle displacement at the generating busses results in undesirable circulating currents.

At one station the simplification shown on Figure 1D was used. In this

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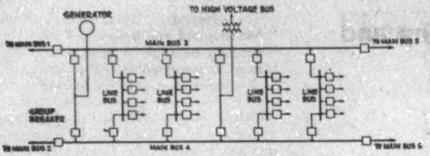
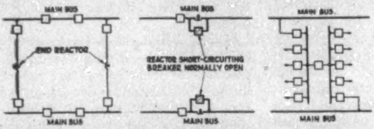
The authors wish to thank those associates in the Commonwealth Edison Company who have furnished valuable information and criticism in the preparation of the paper.



(A) FUNDAMENTAL DOUBLE MAIN BUS LAYOUT



(B) DOUBLE MAIN BUS WITH END REACTORS

(C) DOUBLE MAIN BUS WITH REACTOR TIES BETWEEN BUS SECTIONS

(D) METHOD OF REDUCING NUMBER OF GROUP BREAKERS BY 25%

Figure 1. Double-main-bus layouts for generating and distributing stations

Only operating disconnecting switches shown in diagrams

layout, the line busses are directly connected to only one main bus and each pair of adjacent line busses are tied through a group breaker. This scheme reduces the number of group breakers required for a pair of line busses by 25 per cent. Because of the method of operation used, the loss of flexibility entailed by the use of this scheme is of negligible importance. However, this arrangement required a larger group breaker, inasmuch as a group breaker may be required to supply two line busses.

In the early 1930's, a new distributing station was planned to receive power from the 66,000-volt transmission system for distribution at 12,000 volts. This station was to be built for an ultimate of 80 12,000-volt lines. At the time, operating experience with the conventional double-bus arrangement of Figure 1 was reviewed, and it was found that the number of bus outages was so low that the duplication of busses was not justified. This study resulted in the adoption of the bus layout shown in Figure 2. Two rings are used in this layout because of the sectionalized operating of the 12-kv system. This method of operation is described in another part of the paper.

This new type of bus layout is fundamentally a ring bus in which the distribution lines are connected directly to the main ring. In this way, the main busses used in the conventional double bus are eliminated. The use of this plan results in the elimination of a great deal of bus equipment and approximately 40 per cent of the number of breakers required other than line breakers. In this station five outgoing lines are connected to each bus section, compared to a maximum of four lines per line bus in the older stations. One disadvantage of this bus arrangement is that the supply transformers are connected to the same bus as the lines, whereas in the double bus the transformers are connected to the main busses while the lines are connected to the line busses. The ring bus has an advantage over the straight bus with respect to the concentration of current in the bus. With the ring bus, there is a more equal distribution of the current due to the fact that the supply current can flow in two directions to the load.

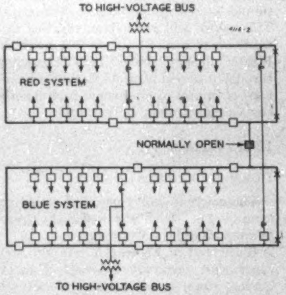


Figure 2. 12-kv ring-bus layout with one breaker between bus sections

Bus sections may be added by cutting ring at points indicated and extending the main rings

The operating experience with the layout of Figure 2 proved to be satisfactory except for one situation which is inherent in all bus schemes which have only one tie breaker between bus sections. With this type of bus separation, trouble in a bus section breaker can cause an outage of two bus sections, thus interrupting a sizable proportion of the entire station. Although such an occurrence is extremely rare, it would be advantageous from a system point of view, if any one case of trouble would cause the outage of only one bus section. This improvement was obtained in a later development shown in Figure 3 by using two breakers in series between bus sections. The only essential difference between Figures 2 and 3 is in the connection between bus sections.

OPERATION OF 12-KV BUSSES

The design of a fundamental bus layout is closely related to the method of operating the system. In the Chicago system, the principle of sectionalization has been one of the corner stones of reliable power supply. With this type of operation any one case of trouble is confined to approximately one-half of the system. All generating stations and transmission terminals are divided into two parts, known as the red and blue systems. At some stations, the two systems are normally operated completely separated from each other. At other stations, the red and blue systems are normally operated in parallel, but automatic sectionalization takes place whenever a heavy fault occurs on either system. This automatic sectionalization, which is accomplished by relay action, separates the station into two parts without causing the outage of any lines or equipment. The method of operating a particular station depends on the magnitude of short-circuit currents and the balance between the load and capacity. It would be desirable to operate sectionalized at all stations, but in some locations it is not economical to provide sufficient equipment to make possible this method of operation.

The ring bus is well adapted to the type of sectionalized operation used in Chicago. In the bus layouts shown in Figures 2 and 3, two rings are used, one ring constituting the red system and the other the blue system. An important advantage of the ring bus is that it can operate sectionalized even though a bus outage occurs. In the double-bus layout, Figure 1A, a main bus outage causes the loss of a considerable amount of equipment, with the result that when such an outage occurs the red and blue systems must be operated solidly tied, and without automatic sectionalization.

Various methods are provided for the normal operation of the two rings. In the diagram of Figure 2, the two rings can be paralleled directly, through reactors, or operated completely separated. In the diagram of Figure 3, the rings may be tied through reactors, or tied solidly through connections to an adjacent station. There is also a tie between the rings through the two windings of the supply transformer; however, this tie is of relatively high reactance and represents a weak link between the systems. The two-winding generator shown constitutes a relatively heavy tie between the systems. Automatic sectionalization is provided to disconnect this generator from the system in trouble. The

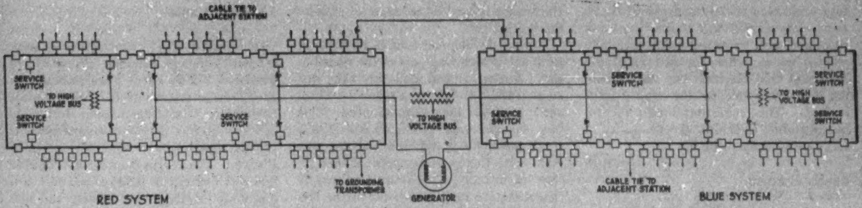

CABLE TIE TO ADJACENT STATION
SERVICE SWITCH
TO HIGH VOLTAGE BUS
SERVICE SWITCH
SERVICE SWITCH
TO HIGH VOLTAGE BUS
TO GROUNDING TRANSFORMER
GENERATOR
SERVICE SWITCH
SERVICE SWITCH
TO HIGH VOLTAGE BUS
SERVICE SWITCH
CABLE TIE TO ADJACENT STATION
RED SYSTEM
BLUE SYSTEM

Figure 3. 12-kv ring-bus layout with two breakers in series between bus sections

method of operation actually used at these stations will depend on the magnitude of fault currents and on the balance between load and capacity as previously mentioned.

Bus Protection

Although relay protection of station busses greatly improves reliability, in the earlier Chicago stations no bus protection was used. The current transformers then available were considered to be too hazardous from an insulation viewpoint to be installed in station busses. Also, the electrical performance of these transformers and the types of relays available were not sufficiently good to make possible a reliable relaying system. On the other hand, because of the methods of construction used bus faults were considered to be too infrequent to justify the added risk incurred by the use of the protective equipment then available. This combination of circumstances resulted in all the earlier stations in Chicago being installed without bus protection.

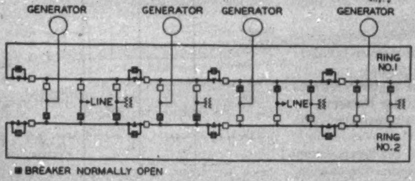

GENERATOR
GENERATOR
GENERATOR
GENERATOR
RING NO. 1
LINE
LINE
RING NO. 2
■ BREAKER NORMALLY OPEN

Figure 4 (left). 22-kv ring-bus layout with provisions for connecting any unit to either ring

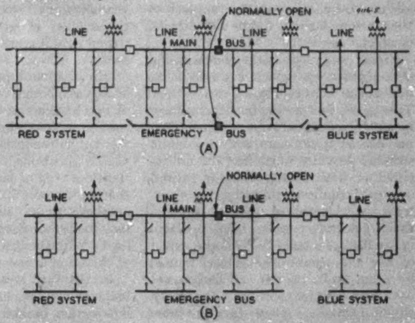

NORMALLY OPEN
LINE
LINE
MAIN
BUS
LINE
LINE
RED SYSTEM
EMERGENCY
BUS
BLUE SYSTEM
(A)
NORMALLY OPEN
LINE
LINE
MAIN
BUS
LINE
LINE
RED SYSTEM
EMERGENCY BUS
BLUE SYSTEM
(B)

Figure 5 (right). Bus layouts used in the early development of the 66-kv system

A—Main-bus sections tied through one breaker

B—Main-bus sections tied through two breakers in series

In the early 1920's, the isolated-phase method of construction was developed and used for all station projects. This method of station construction makes possible a very simple and reliable type of bus protection known as the fault bus.[1] The operating record of this type of bus protection has been very satisfactory, inasmuch as all primary faults have been cleared correctly and quickly. In the past ten years, there have been 12 primary failures on the 66-kv and 12-kv busses at generating stations due to insulator and bushing failures and system operating errors, and these have all been cleared by the fault bus with a minimum of damage and disturbance to the system. In addition to these correct operations, there have been five unnecessary operations caused by low-voltage d-c and a-c circuits coming in contact with the fault bus. Some of the operations have been due to the failure of low-voltage circuits, and others to the improper use of the fault bus by personnel. An example of the latter case is the use of fault bus for grounding welders. These incorrect operations can be reduced to a minimum by using induction relays to prevent operation due to direct current, and by taking precaution to eliminate incorrect use of the fault bus by personnel.

An interesting point in connection with fault-bus protection is its application to the ring bus shown on Figure 2. If a fault occurs on a bus section breaker, the fault bus will instantly disconnect one of the busses to which the section breaker is connected. If this operation does not clear the fault, the other bus section connected to the breaker is cleared. In this manner, a fault on a section breaker may or may not interrupt two bus sections. The possibility of tripping two bus sections for one case of trouble can be eliminated by using two bus section breakers in series as shown in Figure 3. As discussed previously, this undesirable situation also exists in the double-bus layout.

While earlier stations were installed without bus protection, serious bus failures experienced by other companies, together with the development of improved current transformers and relays, have led to the addition of bus protection to these earlier busses. Conventional methods of bus differential relaying using induction overcurrent relays have been used. In one special case, impedance relays have been installed for bus protection. Up to the present time there have been no automatic operations of the bus differential or impedance relay schemes.

22-Kv Busses

The first type of ring bus used in the Chicago area is shown in Figure 4. This bus was installed in a bulk power generating station operating at 22 kv. A detailed discussion of this type of ring-bus layout, which is also used at State Line station, has been published in a previous paper.[2]

This bus is a double ring in which each generator winding and transformer has connections to both rings. The bus sections of each ring are separated by a breaker and reactor in series with provisions to short-circuit the reactor by a second breaker. Experience has indicated that sufficient operating flexibility and reliability can be obtained with fewer breakers than the number shown in Figure 4.

The 22-kv ring busses are oil-filled metal-clad structures which are protected by a fault-bus system. An important limitation of the arrangement used is that a bus section on one ring is part of the same steel structure as the similarly located bus section on the other ring, with the result that the fault-bus system cannot determine which of the two busses is faulted. This condition limits the reliability and, therefore, the flexibility of this type of double-ring bus.

66-Kv Busses

The transmission system within Chicago is a 66-kv underground system which in effect is a cable bus running the full length of the city, interconnecting sources of supply with load distribution centers.[1] This cable bus is operated in two sections known as the red and blue systems which correspond to the 12-kv red and blue systems. The principle of isolated phase construction is used at all the 66-kv terminals and applied to the transmission lines by the use of single-conductor cable.

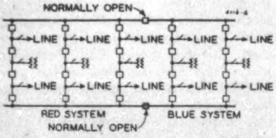


Figure 6. 66-kv bus arrangement which combines the principles of the double-main-bus and ring-bus layouts

Figure 5A shows the fundamental station bus layout which was used in the first busses constructed for this system. This layout is a double-bus arrangement in which there is only one circuit breaker per line or transformer with disconnect switches provided to connect the unit to either or both busses. During normal operating conditions the red and blue systems are sectionalized, with all units on their respective systems being operated in parallel. In order to limit the extent of line and transformer outages caused by a bus fault, the red and blue busses were each split into sections by means of circuit breakers. These breakers were installed only on the main bus since the emergency busses were to be used only when a main bus section was out of service.

An important limitation in the operation of this type of bus layout is that disconnect switches must be operated under load when it is desired to transfer units from one bus to another. Another disadvantage inherent in this type of layout is that a case of trouble on a bus section breaker interrupts a very large amount of capacity. As a result, the bus layout was rearranged to provide two breakers in series between bus sections, as shown in Figure 5B. This principle is the same as is used to limit the outage caused by a failure of a 12-kv bus section tie.

In the development of a new 66-kv terminal, which was part of the distributing station mentioned in the discussion of 12-kv busses, a radically different type of bus layout was adopted. This bus plan is shown in Figure 6 and is a combination of the double-bus arrangement and the ring-bus type of design. One advantage of this arrangement is that a station fault will result in the loss of a minimum number of units. For example, a bus fault necessitates the maximum loss of only one unit; whereas the layout of Figure 5 will cause a loss of two or more units. Another advantage is the increased flexibility of operation; there are many more possible arrangements of the system with a layout of Figure 6 as compared with Figure 5. Although additional line and transformer breakers are required to obtain this increased flexibility, the layout of Figure 6 reduces the number of section tie breakers, so that the total remains approximately the same.

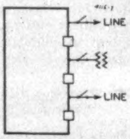


Figure 7. An initial installation of the bus arrangement shown in Figure 6

One disadvantage of the arrangement of Figure 6 is that it requires the opening of two breakers at each terminal to disconnect a faulted line or transformer, but this has not caused any trouble. As previously mentioned, one disadvantage of the older type of bus layout was the fact that to transfer units from one bus to another without de-energizing, it is necessary to operate disconnect switches under load. With the new bus layout, this type of operation is unnecessary.

In the bus layout of Figure 6, all breakers are operated closed except the bus section breakers separating the red and blue systems. The fault-bus system is used to protect the main busses, but the sections of bus to which the lines and transformers are connected are protected by the line and transformer relays. This illustrates one of the basic principles of the ring bus; from the standpoint of protection there is no bus because each section of the bus can be treated as a part of the unit to which it is connected.

In a later development, the bus plan of Figure 6 was adopted as the ultimate diagram. However, in the initial step there are only two lines and one transformer and the layout consists of the ring bus shown in Figure 7.

132-Kv Busses

The high-voltage transmission system outside Chicago consists primarily of overhead 132-kv lines. The principle of group-phase construction is used almost entirely in the transmission terminals of this system. A typical layout used for 132-kv busses on this system is shown in Figure 8. This arrangement is a double bus with one circuit breaker per unit and disconnect switches to connect the line to either of the two busses. A bus tie breaker is provided with necessary connections so that it can be used either as a bus tie or as a unit breaker. This type of bus layout was designed primarily for parallel operation of the 132-kv lines.

At the present time, the 132-kv system is operated sectionalized for the purpose of limiting the effect of faults on the stability of the system. With this method of operation, the type of bus shown in Figure 8 lacks operating flexibility. In order to take a breaker out of service either the bus tie breaker must be used for line protection, or the line must operate without a breaker. In the first case, sectionalization cannot be maintained, while in the second case no relay protection is provided for the line.

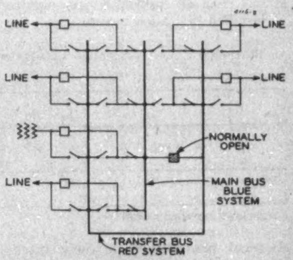


Figure 8. Double-main-bus layout used for 132-kv terminals

Practically all the busses on the 132-kv system were installed without bus protection. Operating experience has indicated the desirability of providing bus protection at these terminals, inasmuch as there has been an average of about one bus fault per year on nine 132-kv terminals of this system. These faults have been due to operating errors in grounding live busses, insulator and bushing flashover, and conductors coming in contact with each other due to insulator breakage. Investigations have been made to determine the feasibility of applying bus protection to these terminals. It was found that fault-bus protection could not be used because of the design of the steel structures. This study also indicated that the application of differential protection to these busses would be very complicated because the many combinations of system connections which are possible make it extremely difficult and costly to install a reliable and high-speed bus differential scheme that will satisfy all operating conditions. As a result of the difficulties involved in providing bus protection, most of these terminals are still operating without this type of protection.

Studies have been in progress recently which indicate that the principles of the ring bus could be advantageously applied to 132-kv terminals. One arrangement which has been studied is the single ring bus shown in Figure 9, which requires only one breaker for each line or transformer. The principal difference between this layout and that of Figure 6 is that the latter uses a larger number of breakers which results in a higher degree of flexibility. In both types of layout, the bus sections are relayed as part of the line or transformer connected to that bus section.

The most important advantage of the ring bus over the arrangement of Figure 8 is that bus protection is automatically provided. Also, a bus fault will affect system stability to a lesser degree if the ring bus is used, because as mentioned previously, the ring bus reduces to a minimum the number of units which must be disconnected for a bus fault. Another advantage of the ring bus is that any breaker in the ring can be taken out of service for maintenance work without disrupting the operation of the remainder of the system. However, if a fault should occur on a line or transformer while this maintenance work is being done, there is a possibility of losing other units in addition to the faulted unit. This characteristic of the ring bus is generally not of great importance during periods of light load when maintenance work is performed, but it does make the single ring unsuitable for use at terminals which are operated with the busses sectionalized. When a line or transformer is to be out of service for a long period, the disconnects on this particular unit can be opened and the ring re-established by closing the breakers of this unit.

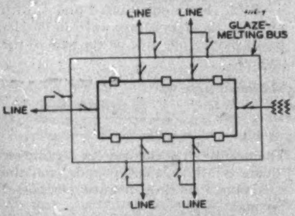


Figure 9. Single ring bus which has been studied for application to high-voltage terminals where all units are normally paralleled

An important problem arises in connection with operating arrangements for the melting of glaze on transmission lines. In the Chicago area, glaze melting is accomplished by isolating generators and transmission lines, and operating them with the transmission lines grounded and with reduced voltage on the generators. With conventional bus arrangements such as Figure 8, glaze-melting procedures may be effected by using one bus for load-carrying purposes and the other bus for glaze-melting purposes. Obviously this could not be done with the ring bus, but the problem can be solved by providing a separate glaze-melting bus with disconnect switches at each terminal for connecting lines to this bus as shown in Figure 9.

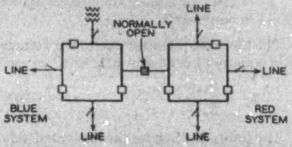


Figure 10. Double ring bus which has been studied for application to high-voltage terminals where sectionalized operation is used

In the conventional type of bus, one set of potential transformers can be used to provide potential for metering and relaying. In order to provide for all possible operating conditions with the ring bus, it would be necessary to use more than one set of potential transformers.

Other arrangements of the ring bus have been studied. Figure 10 shows a double ring bus which requires one additional circuit breaker in order to accommodate the same number of lines and transformers as in a single ring. An advantage of the double ring bus is that a fault on one line does not break the ring. Also, this arrangement is desirable for those cases where the bus is to be operated in two systems, one ring being used for each system, without requiring any more breakers than are used on the double bus arrangement of Figure 8. However, the double ring does not have the same degree of flexibility as the bus layouts of Figures 6 and 8 for transferring units from one system to the other.

The ring bus shown in Figure 11 has desirable features for application at bulk-power generating stations. In addition to the advantages offered by the single ring bus previously mentioned, this layout reduces considerably the number of circuit breakers required without much sacrifice in the reliability of the lines, since transformer faults are very rare compared with line faults. For the case shown, only six breakers are required, while the layout of Figure 9 would require ten breakers.

The most desirable type of bus layout for any specific high-voltage installation depends primarily on the method of operating the system. At a terminal where all the units are to be normally operated in parallel the single ring (Figure 9) is the most suitable. If the terminal is to be operated sectionalized in two parts with only a limited need for transferring units from one system to the other, the double ring (Figure 10) is the logical choice. On the other hand, if maximum operating flexibility is required the principle shown in the bus layout of Figure 6 should be adopted.

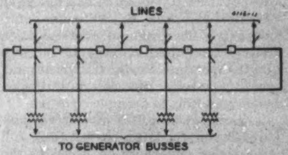


Figure 11. Single ring high-voltage bus which has been studied for application to bulk-power-supply generating stations

This section of the paper has discussed the advantages of applying the ring bus construction to new 132 kv structures. No attempt has been made in this paper to show the cost of rebuilding existing installations to incorporate the ring-bus design, or whether the cost of such rebuilding would be justified.

Conclusion

This paper has described the development of high-voltage bus layouts in the Commonwealth Edison Company. It has attempted to show that the characteristics of the ring bus provide various advantages from the standpoint of flexibility of operation, ease of providing protection, simplicity of maintenance, and economy in investment.

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Interlamination Resistance

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ASSOCIATE AIEE

IN laminated-core structures for transformers and rotating machines the need for adequate interlamination resistance becomes more necessary as the core structure increases in size. Where considerable pressures are exerted on the core laminations, a safe value of interlamination resistance must be maintained to prevent the stray losses in the core from becoming excessive. A method for determination of the minimum interlamination resistance required in terms of the core and its operating conditions, for any degree of permissible stray loss, is developed herein, resulting in a simple equation.

A short paper on this subject was prepared during the latter part of 1943, and encouraging comments on it led to the writing of this paper. It is recognized that this problem undoubtedly has been investigated or considered frequently, privately or otherwise, since the advent of the laminated core. In recent years, however, it has been the author's conviction that a more complete understanding should be had of the major factors influencing stray losses in the core. Although the consideration of the problem is not so rigorous as it could be, it is believed that, for designers of core structures, the resulting equations provide adequate protection in determining the minimum interlamination resistance required.

Figure 1 shows a section of a rectangular core of width W and height of lamination stack aW. The flux density and interlamination resistance are assumed to be uniform throughout the core structure, and the electrical resistance of the silicon steel of the laminations is negligible when compared with interlamination resistance. For analysis the core is divided by a neutral plane into two equal parts, each $W/2$ wide.

The voltage causing the stray-loss currents to flow is perpendicular to the laminations, and its value is a function of the lamination width. Consider an element dx in width, one centimeter in height, and aW long, having a resistance R, with the voltage causing the stray loss to be applied over the element length aW. The position of the element is x distant from the neutral plane of the core, and the core area enclosed becomes xaW. The resulting voltage due to the flux through this area will be that for one turn and will be concentrated across the element length aW. This can be stated as:

$$\frac{E}{N}=\frac{4.44fBaWKx}{10^8}\text{ volts per turn} \qquad (1)$$

where

f = frequency in cycles per second
B = flux density in kilogausses
a = ratio of height of stack to lamination width
W = lamination width in centimeters
K = space factor
x = distance of element from center of core in centimeters

The loss in the element due to the voltage of equation 1 is E^2/R watts, with

$$\frac{E^2}{R}=\frac{(4.44fBaWKx)^2}{R10^{10}}\text{ watts} \qquad (2)$$

The total stray loss for the laminated-core width $x=0$ to $x=W/2$ is:

$$\text{Total watts}=\frac{(4.44fBaWK)^2}{R10^{10}}\int_0^{W/2}x^2dx$$

$$=\frac{(4.44faKB)^2W^5}{24R10^{10}} \qquad (3)$$

Let the resistance

$$R=\frac{raW}{t}\text{ ohms} \qquad (4)$$

for the element having a cross section of one centimeter square and aW long, with r being the interlamination resistance in ohms per square centimeter per lamination and t the lamination thickness in centimeters. The weight of the core for the total stray loss of equation 3 is $0.5aW^2dK/453.6$ pounds, where d is the density of the core material in grams per cubic centimeter. Substitution for R in equation 3 and division by the weight of the core involved gives the average stray loss in watts per pound:

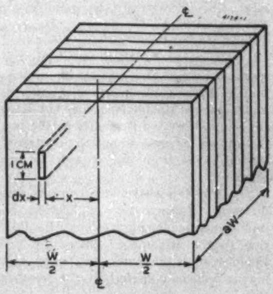

Figure 1. Section of laminated core

$$\frac{\text{Watts}}{\text{Pound}}=\frac{1.2211f^2B^2W_a{}^2Kt_a}{rd10^6} \qquad (5)$$

with W_a and t_a being expressed now in inches. With P in watts per pound for the core material at flux density B and frequency f, the ratio of the stray loss to core loss, stated as a percentage, %, becomes:

$$\%=\frac{1.2211f^2B^2W_a{}^2Kt_a}{rdP10^4} \qquad (6)$$

and if we solve for the interlamination resistance r,

$$r=\frac{1.2211f^2B^2W_a{}^2Kt_a}{\%dP10^4}\text{ ohms per square centimeter per lamination} \qquad (6a)$$

It should be observed that the interlamination resistance r varies as the square of the frequency, the flux density, and the lamination width.

Further analysis shows, however, that the maximum stray-loss voltage occurs at the lamination edge of the core when $x=W/2$. From equations 2 and 4, with $x=W/2$, the stray loss in watts in the element, having one side as the side of the core, is:

$$\text{Maximum stray watts}=\frac{(4.44fBKW)^2taW}{4r10^{10}} \qquad (7)$$

The weight of the element one centimeter square is $aWKd/453.6$ pounds, and the maximum stray loss in watts per pound becomes:

$$\frac{\text{Watts}}{\text{Pound}}=\frac{3.6633f^2B^2W_a{}^2Kt_a}{rd10^6} \qquad (8)$$

with W_a and t_a expressed in inches.

With P in watts per pound for the core loss at B and f, the ratio of the maximum stray loss to core loss, stated as a percentage, %, is

$$\%=\frac{3.6633f^2B^2W_a{}^2Kt_a}{rdP10^4} \qquad (9)$$

and, if we solve for the interlamination resistance r,

$$r=\frac{3.6633f^2B^2W_a{}^2Kt_a}{\%dP10^4}\text{ ohms per square centimeter per lamination} \qquad (9a)$$

If equations 9a and 6a are compared, it will be observed that, with all factors the same, including the percentage, the interlamination resistance as calculated from equation 9a is three times the value obtained from equation 6a. But as the voltage causing the stray loss is maximum at the lamination edge of the core, the interlamination resistance should be calculated for this condition (equation 9a) rather than for the average stray loss (equation 6a). Furthermore, the maxi-

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mum voltage occurs where the lamination-edge condition is an important factor, that is, degree of burring and edge distortion which must be controlled to prevent low interlamination resistance.

If, for instance, in using equation 9a it is assumed that the per cent maximum stray loss to core loss at the lamination edge is nine per cent and the value of r is determined, then the per cent average stray loss to core loss becomes one third or three per cent. This is another way of showing the desirability of using equation 9a rather than equation 6a.

The interlamination resistance r is the average value in ohms per square centimeter of lamination and is the sum of all factors affecting its value. Not only does it include the resistance of the core-plating and surface oxides, but it also reflects the condition of the lamination surface and edges, stray metallic particles on the surface that may short-circuit adjacent laminations, and so forth. Furthermore and of major importance, it is the resistance that must be in effect at the pressure applied to the core and the temperature at which the core operates to assure the percentage of stray loss to be within the value chosen.

In applying equation 9a, the relation of B^2 to P should be examined. An examination of several commercial grades of silicon steel, as used for the laminations in rotating machines and transformers, in the flux density range of 8 to 15 kilogausses, shows that the ratio of B^2 to P can be considered a constant within an error of about plus or minus five per cent. This ratio states that the core loss varies as the square of the flux density, and for practical purposes the error involved is negligible in this application. This ratio also can be considered as a figure of merit for the steel used, as the greater its value the lower the core loss, for a given thickness.

If $B^2/P=C$ (a constant) is considered for the grade and thickness of the core steel used, the number of design curves that need be prepared for ready reference is simplified and reduced. Such curves can be drawn with the lamination width (W) as the independent variable, the interlamination resistance (r) as the dependent variable, with various values of the ratio of stray loss to core loss in per cent (%) as the parameter. The thickness (t), frequency (f), density (d), and space factor (K) are usually the same for many designs, with the first two, (t) and (f), usually not having more than two or three standard values.

Figure 2 illustrates such a design curve, using a 29 gauge (0.014 inch) commercial grade of transformer 52 silicon-steel sheet, with the core-loss-test specimen cut parallel to the grain of rolling and annealed. This test condition simulates the laminations in a transformer, and for this grade of steel $C=B^2/P=222$, being the average value in the flux-density range of 8 to 15 kilogausses. With $t=0.014$ inch, $f=60$ cycles, $d=7.5$, $K=0.92$ and % having values of 0.1, 1.0, 10, 100, and 1,000, the curves of Figure 2 were obtained. Such curves clearly show the values of interlamination resistance necessary to control the stray loss, as well as those values that will allow excessive stray loss with the danger of hot spots developing, further reducing the interlamination resistance progressively to the point where the core laminations may melt, causing eventual failure of the apparatus. Experience in applying these curves to such conditions just mentioned has substantiated the soundness of the analysis.

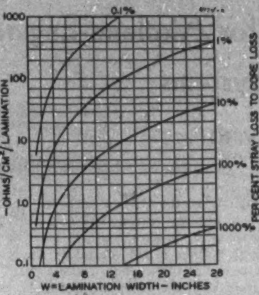

Figure 2. Variation of interlamination resistance with lamination width and per cent stray loss to core loss

For the conditions:

$$r=\frac{3.6633\,f^2B^2W_a^2Kt_a}{\%dP10^4}=\frac{0.50279\,W_a^2}{\%}$$

ohms per square centimeter per lamination

where

$f=60$ cycles $\quad d=7.5$

$K=0.92 \quad B^2/P=222$

$t_a=0.014$ inch

In the case of large transformers where the cores usually are bolted along the middle of the lamination, the pressure is maximum where the stray-loss voltage is minimum, with the pressure at the lamination edges being lower than at the center. By calculating from equation 9a the resistance for the maximum voltage, this value becomes the minimum interlamination resistance for the maximum pressure and operating temperature for the core, thus constituting the limiting and safe conditions. Whatever additional safety factor is desired can be multiplied by this calculated interlamination resistance. The same considerations also apply to the cores of rotating machines.

If the laminations on one side of the core are short-circuited together, the stray loss in the core increases four times as the voltage is doubled on the nonshort-circuited side. Furthermore, if the interlamination resistance is broken down, because of the increased voltage, the stray losses will increase more than four times.

In large symmetrical transformer cores bolted down the center of the laminations short-circuiting of the core by the bolts has no adverse effect, so long as the interlamination resistance is uniform throughout the core. For practical purposes, however, it is desirable to insulate all bolts in case of nonuniformity in the core structure, even though the ratio of total stray loss to core loss of the assembled core is within the allowed value.

The measurement of the interlamination resistance of the finished laminations becomes most desirable as a means of quality control in the manufacturer's plant toward assuring a resistance greater than the calculated minimum. The measurement of this resistance should be made on the specimens subjected to the maximum pressure, the maximum temperature and the maximum voltage to be encountered in the core. The rms voltage is given by equation 10,

$$E=\frac{1.4319fBaW_a^2K}{10^4}\text{ volts} \tag{10}$$

where W_a is in inches.

A standard test method in general use is that for measuring interlamination resistance, as given in the American Society for Testing Materials *Standard Method A-34*. The test specimen is composed of 3x25-centimeter strips that can be processed in the same manner as are the laminations. It is essential that, whatever specimen is used to measure the resistance, it should be prepared in the same manner as are the laminations, so that not only the surface of the specimen but also the edge effects are included in the measurement.

The pressure can be applied readily, and the heating of the specimen can be accomplished by various methods. The testing at temperature is important, especially where core temperatures are higher than those heretofore considered normal, such as encountered in air-cooled inorganically insulated coils and cores. While the organic core-plate varnishes, when properly applied and baked on the lamination, will withstand pressure and voltage at higher core temperatures, the same core plate improperly processed or cured may flow and not give the desired insulation to withstand the voltage and pressure. Inorganic core plates are available, which will withstand lamination annealing temperatures and yield suitable values of interlamination resistance for large cores.

In conclusion it may be said that an equation has been developed wherein the minimum interlamination resistance can be calculated for the maximum stray-loss voltage to be encountered in a uniform core structure for a given percentage of maximum stray loss to core loss. This value of resistance is that to be in effect at the temperature and pressures at which the core structure is to operate. The importance of quality control of interlamination resistance, especially in large

Battery Booster Coils for Airplane-Engine Ignition Systems

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NONMEMBER AIEE

THE use of battery booster coils and problems in connection therewith requires an understanding of engine ignition-system design and operation. Figure 1 illustrates a complete ignition system consisting of ignition switch, two magnetos, radio-shielded harness, spark plugs and booster magneto. One magneto is illustrated completely assembled, and the other in skeleton form showing electric and magnetic circuits.

The magneto primary winding is grounded at one end and connected to the insulated breaker contact at the other end. A capacitor, as in all vibrator systems, is connected across the breaker contacts. The insulated breaker contact and ungrounded side of the primary winding are connected to a terminal on the magneto housing. This terminal is connected to ground through the ignition switch. When the switch is in the "off" position, a direct path to ground is provided the primary current. Therefore, when the breaker contacts open, the primary current is not interrupted, preventing induction of high voltage in the secondary winding. When the switch is in the "on" position, primary current is interrupted by opening of breaker contacts inducing a high voltage in the secondary winding. One end of the secondary winding also is grounded; the other terminates at the high-voltage insert on the coil. Current in the high-voltage circuit is conducted to the central insert of the distributor fingers by means of a carbon brush, from there to the high-voltage segment of the distributor finger, and across a small air gap to the contacts of the distributor block. High-voltage cables in the distributor block then carry it to the spark plugs where the discharges occur.

The distributor finger is secured to the large distributor gear which is driven by a smaller gear located on the drive shaft of the rotating magnet. The ratio between these two gears is always such that the distributor finger is driven at one-half engine crankshaft speed. This ratio insures proper distribution of the high-voltage current to the spark plugs in accordance with the firing order of the particular engine.

Practically all aircraft engines operate on the four-stroke-cycle principle. Consequently the number of sparks required for each complete revolution of the crankshaft is equal to one-half the number of cylinders. The number of sparks produced by each revolution of the rotating magnet is equal to the number of its poles. Therefore, the ratio of speed at which the rotating magnet is driven to that of the engine crankshaft is always half the number of cylinders on the engine divided by the number of poles on the rotating magnet.

Numbers on the distributor block denote magneto sparking order and do not represent the firing order of the engine. Distributor-block position marked 1 must be connected to cylinder 1, distributor-block position 2 to the second cylinder to fire, and so on.

Sparks are not produced until the rotating magnet is turned at or above a specified number of revolutions per minute, at which speed the rate of change of flux linkages is sufficiently high to produce the required primary current and resultant high-voltage output. The speed varies for different types of magnetos, the average is 100 rpm, and the minimum is approximately 60 rpm. This is known as the "coming-in speed" of the magneto.

Starting of airplane engines has always provided a problem from the ignition engineer's point of view, and many ideas have been introduced in an attempt to provide ideal ignition conditions without increasing the weight of the system. A source of external high-voltage current is provided for starting purposes when conditions make it impossible to rotate the engine crankshaft fast enough to produce coming-in speed of the magneto. This may be in the form of a booster magneto, high-voltage booster coil or low-voltage starting vibrator.

The high-voltage booster coil of the vibratory type has superseded the motor or manually operated booster magneto. This is a device for obtaining a high intermittent voltage from a source of steady low voltage, such as a battery, accomplished by electromagnetic induction (see Figure 2). A primary winding of relatively few turns and a secondary winding of many turns are wound on a common

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transformers and rotating machines, is reviewed, and test methods are indicated.

Appendix

Since the preparation of this paper, the author's attention was called to the paper by L. V. Bewley and Hillel Poritsky,[1] wherein a rigorous analysis is applied to stray losses in the core. Their equation 38, giving the stray loss in watts per centimeter of transformer core, was found to be the same as equation 5 given in this paper. As the transformation may be of interest, it is as follows:

$$W=\frac{\lambda y}{24}\left(\frac{\omega\phi_0}{10^8}\right)^2\frac{a}{b} \text{ watts per centimeter of core length} \qquad (38)$$

where

λy = conductivity across the laminations
$\omega = 2\pi f$, with f in cycles per second
ϕ_0 = total flux, peak value
a = lamination width
b = height of lamination stack

The factor a corresponds to W and b to aW, with $\phi_0 = BKaW^2$. The weight of the core one centimeter is $aW^2Kd/453.6$ pounds. Substitution in equation 38 gives:

$$\frac{\text{Watts}}{\text{Pound}}=\frac{\lambda y 4\pi^2 f^2 B^2 W^2 K 453.6}{24d10^{16}} \qquad (38a)$$

The conductivity λy is equal to t/r, and equation 38a becomes:

$$\frac{\text{Watts}}{\text{Pound}}=\frac{4\pi^2 f^2 B^2 W^2 K t 453.6}{24dr10^{10}} \qquad (38b)$$

and with W and t in inches, equation 38b becomes

$$\frac{\text{Watts}}{\text{Pound}}=\frac{1.2224 f^2 B^2 W_s^2 K t_s}{rd10^6} \qquad (38c)$$

The constant 1.2224 of equation 38c is about 0.1 of one per cent higher in value than 1.2211, as obtained in equation 5.

Equation 38c yields the same interlamination resistance as is obtained from equation 5, being based on the average stray loss rather than the desirable value obtained from the maximum stray loss of equation 8. Unfortunately, equation 38 does not call to the designer's attention immediately nor clearly that the interlamination resistance is a function of the square of the lamination width and the lamination thickness, although it shows that the resistance is a function of the square of the flux density and frequency.

Reference

1. INTERSHEET EDDY-CURRENT LOSS IN LAMINATED CORES, L. V. Bewley, Hillel Poritsky. AIEE TRANSACTIONS, volume 56, 1937, March section, pages 344–6.

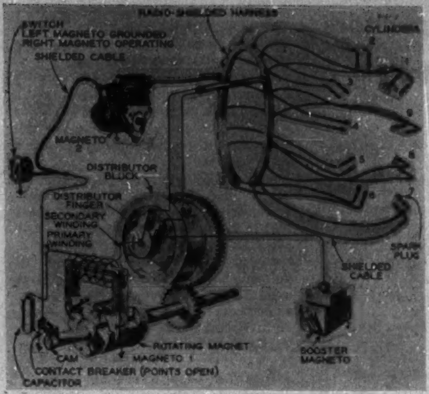
RADIO-SHIELDED HARNESS
SWITCH
LEFT MAGNETO GROUNDED
RIGHT MAGNETO OPERATING
SHIELDED CABLE
CYLINDERS
2
1
3
9
4
8
5
6
7
MAGNETO 2
DISTRIBUTOR BLOCK
DISTRIBUTOR FINGER
SECONDARY WINDING
PRIMARY WINDING
SPARK PLUG
SHIELDED CABLE
ROTATING MAGNET
CAM
MAGNETO 1
CONTACT BREAKER (POINTS OPEN)
CAPACITOR
BOOSTER MAGNETO

Figure 1. Schematic illustration of aircraft ignition system

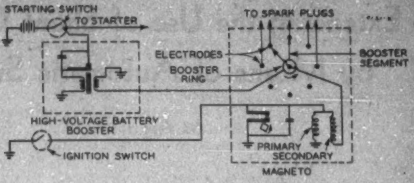
STARTING SWITCH
TO STARTER
TO SPARK PLUGS
ELECTRODES
BOOSTER RING
BOOSTER SEGMENT
HIGH-VOLTAGE BATTERY BOOSTER
IGNITION SWITCH
PRIMARY
SECONDARY
MAGNETO

Figure 2. High-voltage battery booster circuit

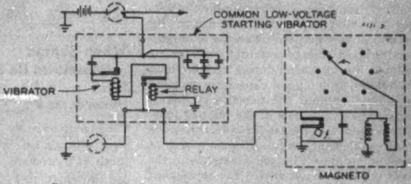
COMMON LOW-VOLTAGE STARTING VIBRATOR
VIBRATOR
RELAY
MAGNETO

Figure 3. Common low-voltage starting vibrator circuit

laminated soft-iron core. A breaker mechanism is in series with the primary winding and battery for the purpose of producing rapid change of flux setting up high-voltage surges in the secondary winding. The secondary winding in turn connects to the magneto distributor segment.

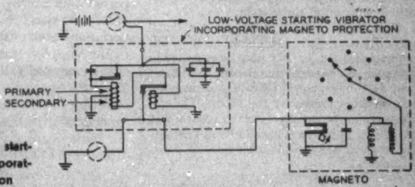
LOW-VOLTAGE STARTING VIBRATOR INCORPORATING MAGNETO PROTECTION
PRIMARY
SECONDARY
MAGNETO

Figure 4. Low-voltage starting vibrator circuit incorporating magneto protection

The battery is connected to the booster coil through the engine-starter engaging switch on inertia starters or motor switch on direct cranking starters (see Figure 2). When the starter switch is closed, the booster coil is energized to provide high-voltage current at the spark plugs prior to coming-in speed. The booster coil fires but one plug in each cylinder; this is the plug giving the least detonation and is usually the front plug on radial engines. This plug usually is fired by the right magneto or the right side of a double magneto. Booster coils are not connected to both magnetos nor to both sides of a double magneto.

The main objection to high-voltage booster systems is the inherent disadvantage in that the trailing finger of the magneto must be placed so far back, to prevent jumping back at altitude, that the starting spark may occur anywhere from 30 to 60 degrees after top dead center. This difficulty has been overcome and correct timing of the starting spark obtained by boosting the main magnetos, thus dispensing with starter magnetos, or high-voltage coils, magneto booster rings and trailing segments. This method consists of using a separate induction coil (low-voltage starting vibrator) connected to the magneto primary winding, utilizing it as a transformer and supplying it with alternating impulses of the order of 150 volts (see Figure 3). It is made up of a coil wound on a soft-iron core connected in series with a breaker operated by magnetic action of the core. A relay is included, in series with and between the booster and magneto, to prevent magneto current feedback through the booster when the booster is not in operation.

A complete wiring diagram of a low-voltage starting vibrator is shown in Figure 3. A battery is connected to the magneto primary winding—through the breaker and vibrator coil. A circuit such as this has one drawback in that no protection is afforded the main magneto in instances when breaker contacts tend to "freeze." "Freezing" of breaker contacts results in high current flow, in some cases 40–45 amperes in a 24-volt system, due to low resistance value of vibrator and magneto windings. High current flow will cause demagnetization of magneto permanent magnets and burning of magneto primary winding. This difficulty has been overcome by using a system illustrated in Figure 4. The vibrator circuit here differs from the common unit in that the coil consists of a double winding having a common core and common ground. The primary winding has more turns than the secondary winding, so that, for a given magneto current requirement, a lesser amount need be interrupted through the primary winding. This is not the case in Figure 3. This feature contributes greatly to longer contact life, and, too, should the breaker contacts "freeze," no induction takes place, and current will flow through the primary winding to ground. Under these conditions no harm can be done to the magneto.

Earlier types of magnetos have magnets of low-coercive-force steel which would lose their magnetism if used with a low-voltage starting vibrator. Engine starting speed, hand cranking, kickback, and required spark retard for starting all have a bearing on use of low-voltage units in place of high-voltage type.

Oscillations in a vibratory system are very complex, involving the primary *LC* circuit, the secondary *LC* circuit and distributor-to-spark plug circuit. Calculations of a coupled circuit are difficult enough in an a-c case; they are more so in the transient case and practically impossible when spark gaps, nonlinear iron, and dielectric losses are combined with free oscillations.

Extending the Use of Shunt Capacitors by Means of Automatic Switching

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ASSOCIATE AIEE

Synopsis: It generally is recognized that the kilowatt loading on distribution circuits or plant feeders may be increased substantially by using capacitors to supply the kilovar load and relieve the current-carrying system of this burden. The use of capacitors to boost voltage or actually to regulate voltage has increased during recent years. Automatic switching of capacitors makes it possible to obtain the benefits of adding capacitors to take care of the full load kilovars without the objection of excess capacitor kilovars at light loads. It also makes it possible not only to apply capacitors in the most effective manner from the standpoint of meeting power-factor limits established by the utility, but also to regulate voltage in the plant or on the system.

Capacitors may be switched automatically by the same switching means controlling the load, or they may be switched in response to changes in circuit conditions such as voltage, current flowing, or the reactive kilovars drawn by the load. The advantages of the various types of control and the choice of the number and size of capacitor steps to be switched are discussed fully.

THE wide acceptance of shunt capacitors and the desire to obtain the benefits of adding sufficient capacitor kilovars to meet the full-load kilovar requirements has resulted in a demand for equipment which will switch them automatically to avoid excessive leading kilovars or overvoltages at light loads. The automatic switching makes it possible to obtain the benefits of additional capacitor correction that otherwise would not be permitted. The effectiveness of shunt capacitors often can be increased by switching them "on" and "off" in accordance with changes in load. This is particularly true on circuits with widely varying loads where it is desired to maintain improved voltage conditions, or hold the current or kilovars supplied by the source to a minimum with variation in kilowatt loading. The demand for switching equipment increases as more and more capacitors are added to compensate, in whole or in part, for the kilovars demanded at full load. In general, capacitors should be switched to prevent overvoltages, to reduce line current, to keep the demand low in proportion to the kilowatt loading, or keep the over-all power factor within desired limits.

When Should Capacitors Be Switched?

OVERVOLTAGE

Capacitors may be switched to avoid overvoltages on lamps, motors, and so forth at light load. Standard capacitors are designed for operation on circuits whose average voltage over a 24-hour period does not exceed the normal rated voltage by more than five per cent. The variations above the average may go up to the accepted standard limit of 115 per cent of normal rating for 230-, 460- and 575-volt capacitors or the standard limit of 110 per cent of normal rating for capacitors with higher voltage ratings.

VOLTAGE REGULATION

Switched capacitors can be located on distribution circuits or plant feeders beyond the voltage regulators to improve voltage regulation by reducing the current flowing in the feeders. This is accomplished without changing existing regulating equipment and lowers the *IX* and *IR* losses in the circuit.

REDUCTION OF POWER COSTS

To maintain minimum cost of power purchased under certain rate schedules, capacitor kilovars should be varied to keep the power factor within certain limits or to keep the demand to a minimum with variation in kilowatt loading.

AVOID OBJECTIONABLE HARMONICS AT LIGHT LOAD

In a few cases it has been found desirable to switch capacitors "off" during light-load conditions because of the combination of objectionable harmonics and overvoltages. These cases are rare, but when they are encountered, it is usually more economical and satisfactory to switch the capacitors "off" automatically during light loads when they are not required than to apply a filter or wave trap.

The decision on whether or not the capacitors should be switched automatically depends upon the benefits expected, the size of the capacitor banks, the extent to which the kilowatt and kilovar load varies over a typical load period, and the variation of voltage with load. The first step in making an analysis usually is to make measurements of kilovars, kilowatts, and voltage over a typical load period to determine the capacitor kilovar requirements and then, if automatic switching is needed, to determine what type of control should be used for actuating the switching device.

Methods of Switching Capacitors

Capacitors may be switched manually with variations in load. Requiring an attendant to make the necessary observations of operating conditions such as voltage, power factor, kilovar demand, and so forth, this method of switching should be considered for those cases where an attendant can be assigned the specific duty of performing the switching operations.

In general, capacitors should be applied as near the load as possible. Therefore, if it is found desirable to switch capacitors with variations in load, one of the first things to consider is the distribution of the capacitors on the feeders and their connection directly to the motor terminals so that they may be switched "on" and "off" with the motor without additional switching equipment. In this connection, the capacitor kilovars switched with the motor switch should be selected to avoid excessive overvoltage due to self-excitation. It is difficult to determine how large the capacitor may be without causing excessive overvoltages unless the motor characteristic curves are available. The majority of motors will generate about 165 per cent normal rated voltage when disconnected from the line with a capacitor whose kilovar rating is equal to the motor horsepower rating. Some motors may generate higher voltages under these conditions. No overvoltage is encountered if the capacitor kilovar is not greater than the motor no-load kilovar.

In some instances it may be desirable to use a separate switch for the capacitor

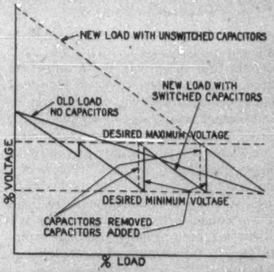

Figure 1. Kilowatt load increased 225 per cent by adding switched capacitors without increasing voltage regulation

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which is controlled by the motor control. If the motors are small or if, due to load factors involved, it is not practical to switch the capacitors with the motors, then automatic switching equipment should be used with the capacitor.

If the load follows the same repetitive load pattern each week, then a time switch may be a practical means of initiating the switching operations. This type of device is not suitable for applications where the load fluctuates widely with a variable load cycle.

Capacitors also may be switched automatically in response to changes in load. There are three generally accepted methods of initiating the switching of capacitors automatically:

1. Voltage control—In response to changes in circuit voltage by means of a voltage-sensitive relay.

2. Current control—In response to changes in load current by means of a current-sensitive relay.

3. Kilovar control—In response to changes in reactive load by means of a kilovar-sensitive relay.

VOLTAGE CONTROL

The automatic switching of capacitors in response to changes in circuit voltage may be considered for applications where objectionable voltage deviations occur with varying loads. It is well known that capacitors will raise the general voltage level on a typical circuit.

To have a clear conception of voltage changes or voltage regulation, it will be helpful to remember that for all practical purposes the voltage drop can be determined from the kilowatt-load component flowing through the resistance of the circuit plus the kilovar component flowing through the reactance of the circuit. The voltage drop caused by the kilowatts is the product of the kilowatt load in amperes and the circuit resistance in ohms. The voltage drop caused by kilovars is the product of the kilovar load in amperes and the circuit reactance in ohms. The total voltage drop is the sum of the two. The voltage rise produced by the capacitor is the product of the circuit-reactance ohms and the change in kilovar-load amperes provided by the capacitor. The voltage change affected by switching a capacitor is the product of the circuit-reactance ohms and the change in kilovar-load amperes produced when the capacitor is switched. In calculation of voltage rise produced by the capacitor above an existing voltage level, the nonlinear variation of exciting kilovars drawn by motors, transformer, and so forth, should be given due consideration. If the voltage level is high, the exciting kilovar load may increase rapidly with increase in voltage and should be taken into account in arriving at the net kilovar load. It also should be remembered that the capacitor kilovar varies as the square of the voltage for a given rating. If the voltage level is moderate and steps are taken to permit only a small change in voltage, the nonlinear variation of exciting kilovars may be of no consequence and may be disregarded. In this case the voltage rise produced by the capacitor is the product of the circuit-reactance ohms and the capacitor current.

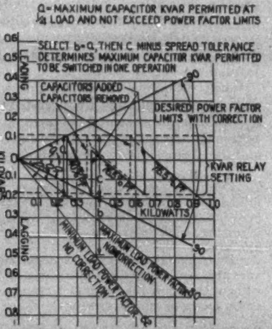

Figure 2. Curves showing relation between kilovars, kilowatts, and power factor, and how capacitors may be switched to keep power factor or kilovar demand within desired limits

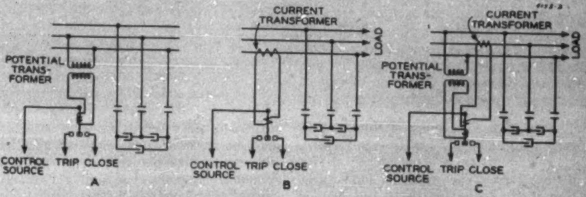

Figure 3. Three methods of automatically switching capacitors

A. Voltage control
B. Current control
C. Kilovar control

The following steps are necessary to determine the amount of capacitors to be connected permanently and the number and size of capacitors to be switched to maintain satisfactory voltage conditions:

1. Determine light-load voltage with total capacitor kilovars connected.

2. If the light-load voltage with the total capacitor kilovars connected is objectionable, then determine the maximum capacitor kilovars permitted during light-load conditions without exceeding the upper voltage limit.

3. Determine maximum-size capacitor that can be switched in one operation or step without exceeding the permissible voltage change.

4. The total capacitor kilovars to be switched is the difference between the total capacitor kilovars required and the capacitor kilovars permitted during light-load conditions.

5. The minimum number of switching steps is the ratio of total capacitor kilovars to be switched to the maximum capacitor kilovars that can be switched at one operation without exceeding the permissible voltage change.

Example. Consider the case of an industrial plant at the end of a feeder where it is desired to increase the load from 442 kilowatts at 65 per cent power factor to 950 kilowatts at 65 per cent power factor without exceeding the current rating of the circuit or increasing the voltage regulation. Power is supplied from a substation over a 2,400-volt three-phase primary feeder consisting of 4/0 cable with equivalent spacing of 33.6 inches, to the 1,000-kva 2,400/460-volt plant transformer bank having 5.4 per cent reactance and 5.55 per cent impedance. The distance from the load center to the transformer bank is approximately 50 feet and from the transformer to the substation is one-half mile. The circuit impedance between the load and the substation as figured on a 2,400-volt base is as follows:

Substation to transformer	$0.15 + j0.315$
Transformer	$0.073 + j0.31$
Transformer to load center	$0.013 + j0.131$
Total	$0.236 + j0.756$ ohm

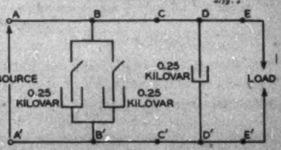

Figure 4. Circuit diagram showing location of load and capacitors referred to in Table I

The kilovars permitted when supplying 950 kilowatts without exceeding the 1,000-kva rating of the plant transformer is 310 kilovars ($\sqrt{1{,}000^2-950^2}=310$).

Table I. Relation Between Power Factor, Kilowatts, Kilovars, and Kilovolt-Amperes for an Assumed Variation of Load Where Capacitors Are Located as Shown in Figure 4 and Switched at Predetermined Values of Load

Load Kilowatt	Capacitor B—B′	Kilovar D—D′	Conditions at E—E′ Power Factor	Conditions at E—E′ Kilovolt-Amperes	Conditions at E—E′ Kilovar	Conditions at C—C′ Power Factor	Conditions at C—C′ Kilovolt-Amperes	Conditions at C—C′ Kilovar	Load Increasing, Conditions at A—A′ Power Factor	Load Increasing, Conditions at A—A′ Kilovolt-Amperes	Load Increasing, Conditions at A—A′ Kilovar	Load Decreasing, Conditions at A—A′ Power Factor	Load Decreasing, Conditions at A—A′ Kilovolt-Amperes	Load Decreasing, Conditions at A—A′ Kilovar
1.0	0.50	0.25	0.707	1.415	1.0	0.80	1.25	0.75	0.97*	1.03	0.25*	0.97*	1.03	0.25
0.9	0.50	0.25	0.707	1.275	0.9	0.81	1.11	0.65	0.986	0.912	0.15*	0.986	0.91	0.15
0.9	0.50	0.25	0.707	1.13	0.8	0.824	0.972	0.55	0.998*	0.802	0.05*	0.998*	0.802	0.05
0.75	0.50	0.25	0.707	1.06	0.75	0.832	0.902	0.50	1.00	0.75	0.0	1.00	0.75	0.0
0.75	0.25	0.25	0.707	1.06	0.75	0.832	0.902	0.50	0.948*	0.792	0.25			
0.7	0.50	0.25	0.707	0.99	0.7	0.84	0.833	0.45				0.997♯	0.702	0.05♯
0.7	0.25	0.25	0.707	0.99	0.7	0.84	0.833	0.45	0.961*	0.728	0.20*			
0.65	0.50	0.25	0.707	0.92	0.65	0.85	0.764	0.40				0.988♯	0.659	0.10♯
0.65	0.25	0.25	0.707	0.92	0.65	0.85	0.764	0.40	0.974*	0.668	0.15*	0.974*	0.668	0.15*
0.6	0.25	0.25	0.707	0.85	0.6	0.863	0. 96	0.35	0.986*	0.608	0.10*	0.986*	0.608	0.10*
0.5	0.25	0.25	0.707	0.708	0.5	0.893	0.56	0.25	1.00*	0.50	0.0	1.00	0.50	0.0
0.5	0.0	0.25	0.707	0.708	0.5	0.893	0.56	0.25	0.894*	0.56	0.25*			
0.45	0.25	0.25	0.707	0.637	0.45	0.913	0.494	0.20				0.993♯	0.454	0.05♯
0.45	0.0	0.25	0.707	0.637	0.45	0.913	0.494	0.20	0.913*	0.494	0.20*			
0.4	0.25	0.25	0.707	0.566	0.4	0.935	0.428	0.15				0.97♯	0.413	0.10♯
0.4	0.0	0.25	0.707	0.566	0.4	0.935	0.428	0.15	0.936*	0.427	0.15*	0.936*	0.427	0.15*
0.3	0.0	0.25	0.707	0.425	0.3	0.986	0.304	0.05	0.986*	0.304	0.05*	0.985*	0.304	0.05*
0.25	0.0	0.25	0.707	0.354	0.25	1.00	0.25	0.0	1.00	0.25	0.0	1.00*	0.25	0.0

* Lagging. ♯ Leading.

The reactive component of the 950 kilowatt load at 65 per cent power factor is 1,110 kilovars (kvar = tan $\theta\times$kw $= 1.17\times950 = 1{,}110$). The expected load variation is from one fourth to full load or $237^1/_2$ to 950 kw. If the measured voltage at $237^1/_2$ kw is 101 per cent and the permissible voltage change is ±2.6 per cent of normal then the size and number of capacitors can be determined, neglecting the nonlinear variation of exciting kilovars, from the following formula:

$$\text{Per cent voltage rise produced by capacitor} = \frac{\text{capacitor kilovars}\times\text{circuit reactance in ohms}}{10\times(\text{line kilovolts})^2}$$

The 800-kilovar capacitor at one fourth load will raise the voltage approximately $10^1/_2$ per cent $(800\times0.756)/10\times2.4^2 = 10^1/_2$, which is excessively high. Since the voltage is one per cent above the mean level without capacitors at one fourth load, then the maximum capacitor kilovar permitted should not increase the voltage more than 1.6 per cent $(2.6-1.0)$. The capacitor permitted at one fourth load is $1.6\times2.4^2\times10/0.756 = 122$ kilovars. Use the nearest standard rating of 120 kilovars.

Since it is desired to keep the voltage within ±2.6 per cent of normal, the capacitor selected should not change the voltage by more than 5.2 per cent minus a margin or tolerance which will prevent the possibility of hunting. If a margin of 0.3 per cent is chosen at both the upper and lower limits, then the capacitor kilovar selected should not change the voltage more than 4.6 per cent. The maximum size capacitor to be switched in one step is 350 kilovars $(4.6\times2.4^2\times10)/0.756 = 350$, and the minimum number of switching steps is two since $(800-120)/350 = 1.94$.

Figure 1 shows graphically the voltage regulation with variation in load for the old load without capacitors and the new load with unswitched and with switched capacitors. The 120-kilovar capacitor representing 15 per cent of the total is permanently connected to give a voltage rise of approximately 1.6 per cent at one fourth load. The remaining 680 kilovars is divided into two equal groups of 340 kilovars and switched automatically in response to voltage to keep the voltage within the desired limits. Each 340-kilovar capacitor switched changes the voltage approximately 4.5 per cent.

Kilovar Versus Current Control

When capacitors are connected to a well-regulated bus, the voltage regulator or other devices usually will keep the voltage within satisfactory limits or attempt to do so independently of the amount of capacitor kilovars applied. Circuit voltage, therefore, does not reflect truly the need for capacitors and, if the capacitors are to be switched, other methods of detecting the capacitor requirements should be used.

If sufficient capacitor kilovars are added to supply the lagging reactive kilovars at full load, then it may be desirable to switch the capacitors with variation in load to obtain maximum benefits by keeping the demand and losses to a minimum or to avoid the possible penalty of excessive leading power factor during light loads. In some instances the kilovars supplied by the capacitor make it possible for the regulating equipment to keep the voltage up to a satisfactory level at full load and in such cases the voltage at light load is very likely to be excessive unless capacitors are switched.

One method to consider is that of switching the capacitors in relation to line current. This is satisfactory and preferred provided the load power factor remains relatively constant with variation in kilowatt loading or the power factor of the load varies in a predictable manner with a variation in kilowatt loading. However, if the load power factor does not vary in a predictable manner, then the capacitor should be switched in response to the load kilovar requirements. When the latter type of control is used, it is not necessary for the load power factor to have a definite relationship with kilowatt loading. In cases where either type of control can be used, the existing physical circuit arrangement may dictate the type to be selected.

Kilovar Control

The kilovar method of control should be used to switch capacitors when the load voltage is regulated and the load power factor varies in an unpredictable manner with variation in kilowatt loading. It often may be used in conjunction with existing voltage regulators to improve voltage regulation. In some instances the existing voltage regulators may not provide satisfactory voltage regulation but when switched capacitors are added to supply the kilovars of the load, the regulators then may provide satisfactory regulation.

When the kilovar type of control is used, the following steps are suggested for determining the amount of capacitors to be connected permanently and the number and size of capacitors to be switched:

1. Determine the capacitor kilovars required at light load to maintain desired power factor or to keep kilovar demand within desirable limits.

2. Determine the maximum capacitor kilovars that can be switched in one operation without exceeding the permissible change in power factor or kilovar demand.

3. The difference between the total capacitor kilovars proposed and the capacitor kilovars permitted duirng light-load conditions determines the total capacitor kilovars to be switched.

4 The minimum number of switching steps is the ratio of the total capacitor kilovars to be switched to the maximum kilovars that can be switched in one operation and stay within the power-factor limits or kilovar-demand limits.

Example. Consider the case of an industrial plant where it is desired to improve the full-load power factor from 70.7 per cent to 97 per cent and thereby reduce the demand approximately 27 per cent or from 1.41 kva per kilowatt to 1.03 kva per kilowatt. The capacitor required to do this is 0.75 kilovar per kilowatt. The expected kilowatt-load variation is between one-fourth and full load and the maximum variation of load power factor without correction is between 62 per cent and 80 per cent lagging. If it is desired to add correction to keep the power factor between 90 per cent lagging and 90 per cent leading for all load conditions, then the boundaries are outlined which will permit the selection of maximum capacitor kilovar at light load.

The kilovars at light load and at maximum uncorrected power factor plus the leading kilovars permitted at light load without exceeding the permissible leading power factor determines the maximum capacitor kilovars permitted at light load or $0.187+0.12=0.309$ kilovar. Once the permanently connected capacitor is determined, this together with the previous boundaries permits the selection of the maximum capacitor kilovars that may be switched automatically in one operation. If 0.3 capacitor kilovar is connected permanently, determine the kilowatt loading at which the 0.3 capacitor kilovar will improve the power factor from 62 per cent to 90 per cent. See Figure 2, line $b=0.3$ kilovar at 0.377 kw. The capacitor kilovars required to improve the power factor at this loading point from 90 per cent to unity plus the leading kilovars permitted at light load is 0.305 kilovar. See Figure 2, line *c*. This determines the maximum capacitor kilovars that can be switched and stay within the power-factor limits. Since the microfarad tolerances of shunt capacitors are usually in the plus direction, and also since the kilovars vary as the square of the voltage, the kilovar relay should always be adjusted for a greater spread than the kilovar rating of the capacitor by a margin sufficient to avoid hunting. If the capacitor kilovars are reduced to 75 per cent of its original calculated value to provide this margin, then the normal capacitor rating should not exceed 0.229 kilovar. The minimum number of automatically switched capacitor steps is $(0.75-0.3)/0.229=1.94$, therefore, the 0.45 capacitor kilovar should be divided into two equal groups of 0.225 kilovar. The kilovar relay may be adjusted to switch the capacitors "on" at 0.175-kilovar lagging with increase in loading and switch capacitors "off" at 0.125-kilovar leading with decrease in loading.

The curves in Figure 2 show power-factor boundaries with and without correction. The two broken horizontal lines show the kilovar relay settings for switching the capacitors "on" and "off" to keep the power factor at all times within 90 per cent lead or lag for load variation of one-fourth to full load. For the one particular variation of loading illustrated, the permanently connected 0.3-kilovar capacitor changed the power factor at one-fourth load from 80 per cent lag to 90 per cent lead. The load increases at a minimum power-factor slope of 40 per cent until the first automatically switched capacitor is added when the reactive reaches 0.175 kilovar. The 0.225-kilovar capacitor changes the power factor to slightly leading and the load increases at a power-factor slope of $76\frac{1}{2}$ per cent until the second automatically switched capacitor is added when the kilovar reaches 0.175. The second 0.225-kilovar capacitor changes the power factor to slightly leading and the load increases at a power-factor slope of $76\frac{1}{2}$ per cent until full-load conditions are reached. On decrease of load the capacitors are switched "off" in the reverse order when the leading reactive reaches 0.125 kilovar as indicated by the broken extended lines. The loading may increase or decrease at different power-factor slopes than those shown and the power factor will be maintained within the desired limits so long as the operating conditions are within the boundaries outlined.

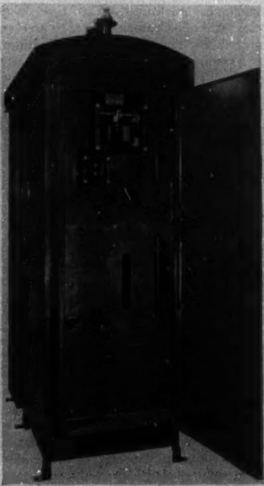

Figure 5. Single-step kilovar control assembled with outdoor capacitor rack showing kilovar relay, two-element time-delay relay, and control switch

If the full load in this case happened to be 1,000 kw, then a total of 750 capacitor kilovars is required, 300 kilovars to be connected permanently and the remaining 450 kilovars to be switched in two equal steps of 225 kilovars.

CURRENT CONTROL

Current control should be considered for switching capacitors where the voltage is well regulated and the power factor of the load remains substantially constant with variation in kilowatt loading or if the power factor of the circuit varies in a predictable manner with variation in kilowatt loading. If in some instance the existing voltage regulators do not give satisfactory voltage regulation, capacitors may be added and switched in response to current to obtain satisfactory voltage regulation in combination with the regulators.

In analyzing the application of capacitors automatically switched in response to current there are always at least two power factors involved and in some instances more. To illustrate how some of the various circuit conditions should be analyzed when more than two power factors are involved, a diagram and tabulation has been prepared. The values shown in Table I are based on the following assumptions:

1. The power factor at point *E–E* remains at 70.7 per cent lagging for the maximum load variation of 25 to 100 per cent.
2. One 0.25-kilovar unswitched capacitor is located at point *D–D* to improve the power factor at *E–E* to unity at minimum load of 25 per cent.
3. Two 0.25-kilovar switched capacitors are located at point *B–B* to improve the circuit power factor at *E–E* to 97 per cent lagging at full load. One capacitor to be switched on at 50 per cent kw load and off at 40 per cent kw load and the second capacitor to be switched on at 75 per cent kw load and off at 65 per cent kw load.

The number and size of the capacitor can be determined in a similar manner to that described for the kilovar control. If more than one switched capacitor is required, a separate current control is required for each capacitor switch. These can be adjusted for different current values to give the same results as multistep kilovar control.

Equipment Required

The automatically switched capacitor station consists essentially of the capacitor, the capacitor switch, the automatic control and instrument transformers, and control source.

THE CAPACITOR

The capacitor usually will be one of the standard ratings and types of mounting

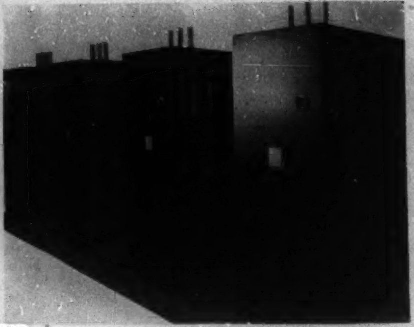

Figure 6. A 1,440-kilovar three-phase 460-volt capacitor divided into four 360-kilovar indoor equipments with electrically operated air circuit breakers and four-step automatic control

which is suitable for the particular location and circuit requirements.

Capacitor Switch

The capacitor switch should be equipped with overcurrent trip devices unless other overcurrent protection is provided. If the switch is equipped with overload and fault-current protection, then it should also have a lockout feature to prevent the switch from closing, if tripped from overcurrent, until the lockout device is reset manually. Of course, if the switch does not have overcurrent trip devices, then no lockout feature is required and the master breaker or line fuses should provide the overcurrent protection.

The capacitor switch should be suitable for repetitive duty and should be selected to have the correct rating as to voltage, continuous current, momentary current, and interrupting ability. Contactors with line fuses or overload attachments may be considered for switching small low-voltage capacitors where the fault current and momentary inrush current does not exceed the maximum momentary rating of the contactor. Oil-immersed contactors may be used on small high-voltage capacitors on a similar basis.

Electrically operated circuit breakers generally are used for switching the larger size capacitor equipments. The breaker should, of course, be selected to have the proper voltage, continuous current, momentary current, and interrupting ability. On the very large banks of the higher voltage ratings the possibility of restriking when opening the circuit to the capacitor is greater and this should be taken into consideration when selecting a breaker.

It is often necessary to have current transformers in the circuit to the capacitors as a means of energizing breaker overcurrent trips. If the transient inrush current is high when the capacitor switch is closed, then sufficiently high voltage may appear across the secondary winding to necessitate a protective device to limit the voltage to a safe value and avoid damaging the insulation in the secondary of the current transformer circuit. This is particularly true on large capacitor banks closely coupled.

Automatic Control

The automatic control for switching capacitors usually consists of a master element, time-delay relays, auxiliary devices, and control switch. A voltage-sensitive relay, current-sensitive relay, or kilovar-sensitive relay may be selected as the master element for initiating the switching operations. The time-delay relays will allow sufficient time to prevent unnecessary switching operations, caused by momentary disturbances on the power circuit. The time-delay desired usually is not critical, but in the interest of keeping the transient current to a minimum, sufficient time delay should be chosen between the opening and closing of the capacitor switch to allow the discharge device to drain off a substantial part of the stored energy before the next switching operation. The auxiliary relays respond to the request of the master element and time-delay relay combination to close or open the capacitor switch. Control switches usually are desired to permit opening and closing of each capacitor switch by manual operation of its control switch or have the capacitor switch open and close in response to the master element.

Instrument Transformers and Control Source

A potential transformer is required for voltage control, a current transformer is required for current control and both current and potential transformers are required for kilovar control. See figures 3A, 3B, and 3C for their positions in circuit. In all cases a single-phase control source is required. Suitable instrument transformers and control source may or may not be available at the proposed capacitor location.

Description of Operation

Single-Step Control

If the control switch is in the automatic position and one set of the master-element contacts close, indicating that capacitor kilovars are required, this will energize the closing element of the time-delay relay. If the initial cause is not a temporary disturbance, then the master-element contacts will remain closed until the time-delay relay contacts have closed energizing the closing mechanism of the capacitor switch. The capacitor switch will then close and connect the capacitor to the circuit.

If with the capacitor connected to the circuit one set of the master-element contacts close, indicating that the capacitor correction should be removed, this energizes the tripping element of the time-delay relay. If the master-element contacts remain closed for the time required for the time-delay tripping contacts to close, then the capacitor-switch tripping device will be energized opening the capacitor switch and disconnecting the capacitor from the circuit.

Multistep Control

The multistep control operates in a manner similar to the single-step control except that it is arranged to switch two or more groups of capacitors selectively and automatically by means of a time-delay relay for each separate group in combination with auxiliary contacts on the capacitor switches. The time-delay relays operating in conjunction with the auxiliaries of the capacitor switches set up the circuits to close or open the capacitor switches in a definite sequence responding to the master element with time delay between switching operations.

Conclusions

1. The usefulness of shunt capacitors can be increased by switching them automatically.

2. The benefits of adding capacitors to supply kilovars up to full-load conditions can be obtained, if automatic switching is applied to remove the objectionable leading kilovars at light load.

3. The type of control to use and the size and number of automatically switched capacitors steps required to do a particular job can be determined when the operating conditions and requirements are known.

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Magnesyn Remote Indication

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REMOTE indication is, in general, the transfer of information or intelligence from one point to a remote point in a semipermanent form which may be visually observed. A variety of means can and have been used to accomplish such a purpose. Information can be sent from one place to another simply by moving a long string or cable, rotating a long flexible shaft, changing pressure in a hydraulic or pneumatic line, changing the characteristics of an electric circuit, as well as by methods involving thermal and optical devices. The fields of telemetering and visual recording are well-known branches of remote indication. Automatic control systems and computing mechanisms depend on remote indication for their source of intelligence.

As aircraft have increased in size and complexity, improved methods of remote indication have become most essential. Practically all information concerning the performance and conditions of engines and ship that is brought to the attention of the pilot comes to his instrument panel by means of remote indication. Relatively large distances exist between pilot and flight-instrument sensing elements, between flight engineer and engines, and between navigator and compass. Pneumatic and hydraulic methods are still most efficient for some functions, but a majority of indications are carried out electrically on modern aircraft. In many cases electric systems can offer a saving in weight, simplicity of installation and maintenance, greater accuracy, better interchangeability of components, reduction in military combat hazard, and a somewhat more inexpensive unit. In some cases electric systems have been the only practical way of remotely indicating a particular function.

Electrical methods of remote indication, as used on aircraft, can be divided into four general groups. D-c systems, using a variable resistance for a transmitter and a voltmeter for an indicator, were probably the first form to be used. This method is still widely used for temperature indication where the variable-resistance transmitter becomes a temperature "bulb," utilizing the inherent change in resistance with temperature of an electric conductor.

Autosyns* have been extensively used on aircraft since 1932 as an a-c electric remote-indicating system similar to the self-synchronous motors used in marine order signal systems and many industrial applications. A transmitting Autosyn which has a rotor mechanically actuated by the function to be indicated is electrically wired to one or more similar Autosyns as indicators with pointers and dials. Every motion of the transmitting rotor is reproduced as a motion of the indicating pointer. The prime mover may be any of the engine-pressure functions, liquid-level functions, or position functions of any movable part on the aircraft.

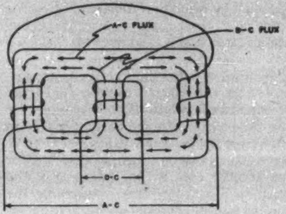

Figure 1. Frequency doubler

A third class of remote indicating instruments are the d-c Autosyns. Operation is analogous to the a-c Autosyn, where the a-c field is replaced by a d-c field. The transmitter is a continuous circular resistance with three equally placed taps contacted on a diameter by a direct voltage. When connected to an indicator similarly wound with a continuous tapped winding on a magnetic iron core, a d-c field will be set up across a diameter of the indicator corresponding to the diameter of the transmitter that is contacted by the d-c voltage. The rotor of the indicator can then be a permanent magnet which will align itself with the indicator field.

A more recent development, which has become well suited to the requirements of aircraft remote indication, is the Magnesyn system. Originally applied to the problem of remote compass operation, it is equally adaptable to most of the functions which are remotely indicated on aircraft. It retains the advantages of a simple indicating unit, as used in the d-c Autosyn system, but utilizes a transmitting unit that is equally simple. The Magnesyn system is a-c operated and uses identical units for transmitter and receiver. In each case, the stator is a toroidally wound tapped coil on a laminated magnetic core, and the rotor is a permanent magnet. Rotation of the transmitter rotor causes corresponding rotation of the indicator rotor.

The preceding four systems are the only ones that have been used in quantity on aircraft. Other electric remote-indicating systems that have been considered are the distometer and the inductance bridge.

In order to understand the somewhat involved magnetic circuit of the Magnesyn, it is perhaps worth while to consider some of the chronological steps in its development. A well-known phenomenon basic to the operation of the Magnesyn is the production of harmonic voltages in a saturated reactor. Such a reactor, shown in Figure 1, is often used as a frequency doubler. An *E-I* lamination core of a magnetic material that saturates abruptly is wound with coils on the outside legs, series aiding, and a separate coil on the center leg. If a sufficiently large alternating current is passed through the outside windings, the magnetic path made up of the outside legs and top and bottom sections will be alternately saturated and unsaturated with magnetic flux twice every cycle. When direct current is passed through the center winding, the magnetic path presented to its field is made up of the two outside legs in parallel. Consequently the magnetic path of the d-c circuit is varying in permeability twice for each cycle of alternating current on the outside legs. This means that the flux passing through the d-c leg will vary from a maximum to a minimum twice for each a-c cycle, inducing a voltage in the d-c winding at even harmonic frequencies of the exciting voltage. The a-c circuit on the outer legs is acting as a "gate" on the amount of flux produced by the d-c field.

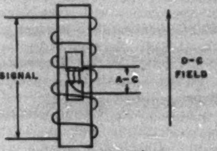

Figure 2. Flux gate

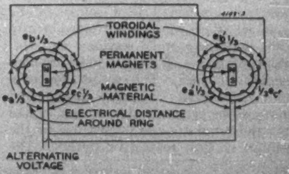

Figure 3. Magnesyn transmitter and receiver

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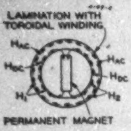

Figure 4 (above). Magnesyn magnetic circuit

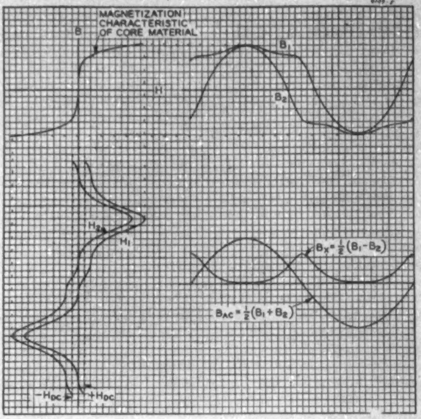

Figure 5 (right). Flux densities and corresponding magnetizing force

By replacing the field from the d-c winding by a permanent-magnet field, or even by the earth's magnetic field, the same principle of operation will apply. Early forms of a unit that operated in the earth's field were called "flux gates".* Construction of this form of frequency doubler is shown in Figure 2. An a-c winding on a portion of a strip of magnetic material serves to saturate the metal twice every cycle, thereby providing a path of variable permeability. Flux produced in the strip by the earth's field will then vary twice each cycle with the varying permeability, which, in turn, will produce an even harmonic voltage in a second winding around the strip. This action is basically the same as that of the original frequency doubler.

A further step in development was the idea that if fundamental flux in combination with a unidirectional field would produce an even harmonic flux in the aforementioned devices, then a fundamental flux in combination with even harmonic flux might be expected to produce a unidirectional field. Such proved to be the case and resulted in the Magnesyn system. Figure 3 shows the elements required for a Magnesyn transmitter and receiver or indicator. Both units can be identical electrically and magnetically. The stator of each is a circular core of magnetic material around which is placed a single continuous toroidal winding which is connected to a source of a-c voltage. Each winding is divided by two taps into three equal sections, and corresponding taps of the two units are connected together. The rotors are two-pole permanent magnets, magnetized along a line perpendicular to their axis of rotation, and are free to revolve in the centers of their respective stators. The Magnesyn is again a frequency doubler supplied by a unidirectional field from its rotor and presenting a variable-permeability path to this field because of the a-c flux produced by the winding.

By first considering the magnetic circuit of the transmitter, as shown in Figure 4, some of the relationships necessary for the production of even-harmonic flux in the stator can be seen. A magnetomotive force, H_{d-c}, will be set up in each half of the annular core by the presence of the permanent-magnet rotor. Assuming the core to be a homogeneous material, symmetrical in shape and concentric to the magnet, the reluctance of the two paths will be the same and H_{d-c} will be equal in the two halves of the ring.

When the transmitter winding is connected to a source of sine wave alternating voltage, the current drawn will produce an alternating magnetomotive force, H_{a-c}, which will be considered at a specific time to have a clockwise direction in the core. With respect to this exciting magnetomotive force, H_{a-c}, the unidirectional magnetomotive force is positive in one half of the core and negative in the other half. Then the total magnetomotive force in one half is

$$H_1=H_{a-c}+H_{d-c} \tag{1}$$

and in the other half is

$$H_2=H_{a-c}-H_{d-c} \tag{2}$$

A typical relationship between flux density produced in the core and magnetomotive force set up in the core is shown as the magnetization curve in Figure 5. By projecting a time cycle of H_1 and H_2 on this characteristic, the corresponding time cycles of flux densities B_1 and B_2 in each half of the core may be obtained. Flux density B_1 is made up of two components, B_{a-c} produced by the exciting voltages which is the same in both halves of the core, and an additional flux density B_x which exists because of the presence of H_{d-c}, combined with H_{a-c}, or

$$B_1=B_{a-c}+B_x \tag{3}$$

Likewise, the flux density in the second half of the core

$$B_2=B_{a-c}-B_x \tag{4}$$

From equations 3 and 4

$$B_{a-c}={}^1/_2(B_1+B_2) \tag{5}$$

and

$$B_x={}^1/_2(B_1-B_2) \tag{6}$$

The time relationships of B_{a-c} and B_x are shown in Figure 6, where it is clear that B_x is an even-harmonic function of B_{a-c} with a major second-harmonic component. Because voltage is induced in a winding surrounding a piece of magnetic material proportional to the rate of change of flux density in the material, component voltages E_{a-c} and E_x will be induced in the transmitter stator winding by B_{a-c} and B_x as shown in Figure 6. Total voltage induced by the axis of the magnet poles on the rotor then will be

$$E_1=E_{a-c}+E_x \tag{7}$$

and in the other half will be

$$E_2=E_{a-c}-E_x \tag{8}$$

The sum of E_1 and E_2 or $2E_{a-c}$ is equal to the impressed a-c voltage.

If we refer to Figure 3, the proportion of even-harmonic voltage appearing in each of the three sections of winding will depend on the position of the rotor. Figure 7 shows the relative amounts of this component voltage in each section as the magnet is rotated through 360 degrees. The fundamental component of voltage in each section, of course, is independent of magnet position and is equal to one third of the total applied voltage.

If taps on the transmitter are connected to the corresponding taps on the receiver stator, omitting the receiver rotor magnet for the moment, and both coils are sup-

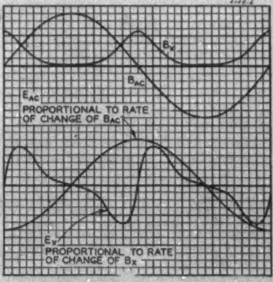

Figure 6. Flux density and voltages in the Magnesyn

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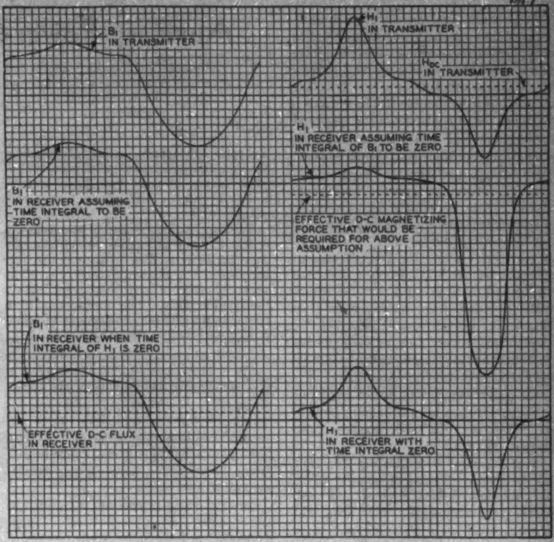

Figure 8. Development of poles in the Magnesyn indicator

plied from the same a-c source, there will be no flow of fundamental current between the two units, because the voltages at the taps of each are at the same fundamental potentials. However, because there are even-harmonic voltages present in each section of the transmitter but none in a receiver with no magnet, there will be a flow of even-harmonic currents between the two coils. This flow of current effectively lowers E_x at the transmitter to some value, E_x', at each instant of time, and produces the same voltage, E_x', in the receiver winding in a corresponding position with respect to the three sections of winding.

It is difficult to obtain the actual value of E_x', but the action of the Magnesyn system can be none the less faithfully followed if E_x' is considered to be the same as E_x, which is the same as saying that B_1 and B_2 in the transmitter core are unchanged when the receiver stator is connected. Because the voltages in the receiver-stator winding have to be the same as those in the transmitter winding as a result of their interconnection, the flux densities in the receiver core must have the same rates of change. This means that B_1' and B_2' in the receiver must have the same time-cycle wave shape as B_1 and B_2 in the transmitter. Figure 8 shows B_1 in the transmitter with its associated H_1 followed by an assumed B_1' in the receiver with a time integral of zero and the H_1' in the receiver that would be required to produce the assumed B_1'. Such an assumption obviously presumes the presence of an effective unidirectional magnetomotive force in the receiver core which is impossible, inasmuch as there is no source of direct current anywhere in the system, nor is there any unidirectional field applied at the receiver. This delimits B_1' in the receiver to such a value that the time integral of H_1' in the receiver is zero, which in turn requires an effective unidirectional component of flux density to be produced in one half of the receiver core. A similar argument will show that a corresponding unidirectional flux-density component exists in the other half of the receiver core. Therefore, an effective unidirectional field will exist across a diameter of the receiver core, corresponding to the diameter in the transmitter in line with the rotor magnet poles. As the transmitter rotor is turned, the effective unidirectional field in the receiver stator will turn likewise.

If a permanent-magnet rotor is introduced at the receiver, a restoring torque gradient will make it align itself in the unidirectional field at the receiver. When it is in line with this field, exactly the same even-harmonic voltages will be induced in both transmitter and receiver coils, and the flow of even harmonic currents will cease. Rotation of the magnet in one unit then will cause corresponding rotation of the magnet in the other unit, and, since they are identical, either unit can act as a transmitter.

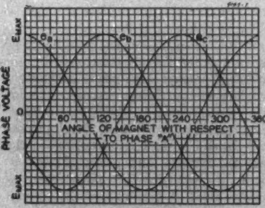

Figure 7. Phase-voltage variation

Because of the nature of the parts involved, the Magnesyn is an inherently simple device to fabricate. All laminations can be annular-ring punchings from sheet metal. The windings are readily adapted to an automatic machine operation. A round disk magnet is used for the rotor and the entire rotor assembly is no more than this magnet pressed on a suitable shaft. In practice, an additional annular core of magnetic material is placed around the outside of the stator serving the dual functions of a return path for even-harmonic flux to the stator and a magnetic shield against extraneous magnetic fields that may be present near the unit.

Perhaps the most outstanding feature of the Magnesyn system is the extremely low reaction torque on a prime mover. In any system the torque gradient of the prime mover must be sufficiently large to mask any friction or error torque gradients in either transmitter or receiver. Any friction torque of brushes or sliding contacts, as well as the bearing friction of rotor weight, must be overcome. Magnesyns have no source of friction except bearings and with jeweled pivots supporting a total rotor weight of the order of one gram, this source of friction is negligible to the most sensitive prime movers. Error torques likewise can be kept to a low value without excessively involved assembly techniques.

Some applications of remote indication in aircraft require the transmitting unit to be installed at a point of extremely high vibration. The construction of a Magnesyn can be modified to make it admirably suited to this requirement. The stator, associated lead wires, and electric connector can be molded literally into one solid unit. The rotor can be mounted on a rugged shaft, with suitable bearings to withstand anticipated vibration. It is even possible to mount the magnet on the prime mover directly and mount a molded Magnesyn stator concentrically to the prime-mover shaft. Such units have been constructed that are waterproof, dirtproof, and oilproof, and that withstand temperatures from −100 to +200 degrees centigrade, with sustained vibration of 100 gravity in any direction. There is no possibility of wear external to the prime mover and no hazard of sparking contacts in an explosive atmosphere.

For certain applications, it is not necessary to have the transmitters and indicators physically the same. This permits remote indication of any moving member by placing a magnet on it and providing a stator at a reasonable distance away in the most convenient part of the assembly.

Some general characteristics of the system are freedom from errors resulting from voltage variation and temperature changes. Accuracy can be maintained at any desired level with suitable choice of transmitter components. Transmitters can be constructed to drive any number of receivers. Units for aircraft use are operated at 26 volts from a 400-cycle source and draw about 1.5 volt-amperes. When operated from an inverter, these Magnesyns will perform satisfactorily on frequencies between 150 and 1,200 cycles, provided the ratio of frequency to voltage is maintained between 13 and 17.

The most widespread use of the Magnesyn to date has been in remote-indicating compasses. In modern combat aircraft it has been essential to place the compass element in a location free from the magnetic effects of armor plate, machine guns, turrets, engines, and other interfering components, and bring the compass heading to the pilot and navigator by remote indication. A single magnet mounted in a compass float serves both the purposes of aligning itself in the earth's field and acting as the rotor of a Magnesyn transmitter. Below the liquid cavity containing the compass float is a Magnesyn stator. This stator is connected to a receiver, and the compass heading is reproduced by a pointer on a dial. No moving parts are added to a conventional magnetic compass.

A class of flight instruments, including air speed, rate of climb, and altitude, has been successfully remotely indicated with the Magnesyn system. These prime movers, in general, are too weak to operate other systems. Practical trailing "bombs" containing these flight-sensing elements have used Magnesyn indication. Remotely operated weather instruments have incorporated the Magnesyn system to advantage.

For indication of the position of various moving members, such as wheels, a variety of flaps, bomb-bay doors, and so on, the shockproof construction type of Magnesyn is well suited. Vibration, dirt, water, and ice have little effect on the molded unit. A particular application where the Magnesyn has stood up remarkably well, is the indication of propeller pitch. Here the position transmitter is mounted in the hub of a propeller and subjected to extreme vibration.

Fuel-level and fuel-flow instrumentation in general presents the requirement that the electric system be external to any cavity containing gasoline vapors as a safety precaution. Most systems accomplish this by having the prime mover in the gasoline drive a magnet which, in turn, drives a follower or slave magnet outside the compartment. By using a Magnesyn stator in place of the slave magnet, the transmitter is complete with no additional equipment.

The development, method of operation, and some features of the Magnesyn have been described with a comparison of other existing electric systems. Magnesyns can be used in a variety of remote-indicating devices, some of which were impractical previously. Future developments will depend on new requirements in the aircraft industry and potential applications in industrial instrumentation, as well as the ever-present problem of continuous improvement in existing equipment.

Auxiliary-Power-Plant Requirements

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Synopsis: Designers of electrical equipment for aircraft are confronted with many limitations. Some of these limitations may be nullified with the use of auxiliary power plants.

To cover various phases of application a series of engine–generators would be necessary. A large number of their design features should be adapted to each type of airplane in order to save weight and obtain best performance. This necessitates close co-operation between designers of the component parts of the airplane.

In developing and extending the use of electric equipment for aircraft, designers have been faced with the eternal problems of space and weight limitations. Two places where a great many limitations have been encountered are at generator and battery.

In most cases generators are mounted on accessory sections of main engines. Diameter is limited by the presence of other accessories, and overhang is limited by the strength of the mounting flange. Another undesirable feature, which has precluded the use of alternators, is the wide speed range of the drive.

Batteries, as a source of auxiliary power, have been a millstone around the necks of designers. With the airplane on the ground the accessory system is almost useless unless ground power units are available. Radio and other accessories can be operated only momentarily without reducing the battery charge to the extent that the main engines cannot be started.

Generator designers have done an excellent job in recent years. They have succeeded in multiplying the output of generators several times without exceeding the rigid space and drive limitations. Although we may expect further development, it is not likely that it will be so rapid.

Batteries also have been improved; however the fundamental shortcoming which characterizes them cannot be overcome. They cannot deliver power beyond their ability to store it. It would be impractical to carry sufficient batteries for supplying auxiliary power in warm weather and impossible in cold weather.

Elimination of some or all of these stumbling blocks will enable electrical engineers to exploit further the use of electricity throughout the airplane. One solution lies in the use of one of the three general types of auxiliary power plant which are as follows:

1. Battery chargers.
2. Battery replacers.
3. Continuous duty (providing a primary source of power).

A further breakdown can be made as to the type of electrical output. In this paper it will be done only insofar as the necessity arises.

Battery Chargers

Without altering the accessory system, except for the addition of a little weight, a battery-charging auxiliary power plant can expand its efficiency materially. One main difficulty thus is overcome; batteries can be recharged readily innumerable times independently of the main engines.

The engine itself must be of simple and rugged construction and as small as possible, consistent with the desired charging rate of the unit. For this type of operation a single-cylinder air-cooled two-cycle engine is a good choice since the number of parts inherently is kept to a minimum. The governor need be no more complicated or accurate than the pneumatic-paddle type used on small engines. A flywheel magneto offers the advantage of having a large part of its

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weight performing useful work as flywheel inertia. Battery ignition, if lighter, can be used since the number of batteries with which the unit operates hardly can be discharged to the point where sufficient voltage for ignition is not available. There is no need for a fuel pump if the unit can be located slightly below the normal fuel level. However, too much fuel head may complicate the carburetor float-valve arrangement. The choice of cooling system depends somewhat upon the requirements of the generator.

A unit of this type has been in use for over a decade in the Navy's flying boats. It consists of a small one-cylinder two-cycle engine driving a combination a-c and d-c generator and has provisions for attaching another accessory. Its long record speaks for itself.

Battery Replacer

The great strides made in the development of generators for main engines resulted in a set back for the continuous-duty type of auxiliary power plant which was being developed to overcome, among other things, low generator output. The resulting increase in electrical loads on airplanes made batteries even less adequate as a source of auxiliary power and led to the development of an auxiliary power plant for replacing batteries.

The battery-replacing type, like the battery charger, is best suited to military aircraft and flying boats, as its main purpose is to provide a more adequate source of auxiliary power where ground-type units are not available. Development of its inherent advantages will make it a common sight in postwar airplanes of all types.

Figure 1 indicates an approximate battery-output curve for a given load as compared with the output curve of an auxiliary power plant. Assuming that the efficiency of the two systems is approximately equal (that is converting of British thermal units in gasoline to kilowatts of auxiliary power) an obvious advantage is apparent.

The output of this type of unit is determined by the largest necessary continuous load required. Although it would be desirable to have sufficient output to handle breakaway surges and to operate several accessories simultaneously, the weight would be objectionable. Figure 2 illustrates roughly the demand curve of an electrical accessory as compared with the output curve of a battery-replacing auxiliary power plant of proper size. Note the shaded area indicating the energy to be supplied by the remaining battery

The engine for the battery-replacing auxiliary power plant is larger than for the battery charger and naturally offers a wider choice as to arrangement. The number of cylinders may vary from two to five and be arranged radially, vee, opposed, in line, or in almost any conceivable manner that results in a compact accessible engine. Either the two- or four-stroke cycle may be used; operating conditions favor the four stroke somewhat. Magneto ignition is desirable and smaller engines of the group may find a weight advantage in a flywheel magneto. The fuel pump should be eliminated, if possible, unless combined with a fuel injector which becomes more practical on larger engines. From the standpoint of first cost and servicing, consideration must be given to the possibility of designing a majority of parts for use in a series of models having varying outputs. This should not be done to the detriment of weight and performance. Controls and other equipment must be determined by the requirements of each installation.

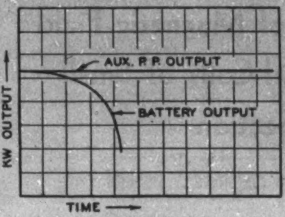

Figure 1. Auxiliary power plant versus batteries

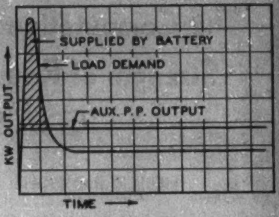

Figure 2. Division of load between auxiliary power plant and small battery

Continuous Duty

The third type of auxiliary power plant, previously mentioned briefly, is one which runs continuously providing a primary source of power. Although certain units of this type have given good service, their development was not advanced sufficiently to compete with improved main-engine-driven generators, and manufacturers turned to the second type mentioned.

Prospects of return of the continuous-duty type are likely when the advantages are considered; some of these are as follows:

1. It rounds out the power system into a complete unit independent of main engines and ground equipment.
2. High-voltage alternators can be used individually or in parallel.
3. Congestion of main-engine accessory sections can be relieved.

Further elaboration on these advantages is unnecessary since they have been discussed many times in various papers. However, one substantial disadvantage does bear mentioning at this time: auxiliary power plants require essentially the same maintenance as main engines do.

Performance records indicate that time between overhauls beyond 1,000 hours is reasonable. This fact coupled with an engine designed for easy maintenance will minimize this objection materially.

The engine for the continuous-duty type of unit generally will be larger than either of the foregoing types, but in many cases it will be identical with the one used in the battery-replacing unit, except for controls and supercharging. For the extreme altitudes at which operation will be required external supercharging has many advantages. Supplementary cooling will be necessary and may be obtained from an air scoop.

As the size of airplanes and, consequently, the demand for auxiliary power increases beyond a certain point, another primary source of auxiliary power may be developed in the form of a steam turbine. Recent trends toward using exhaust gases for deicing as well as for turbosuperchargers might preclude their use as heat for generating steam. This should not hamper the system a great deal since a separately fired boiler would be desirable to provide power independently of main engines.

In order to have auxiliary power plants meet the requirements in the best possible way, close co-operation between manufacturers of airplanes, main engines, auxiliary engines, and accessories (electrical) will be essential.

One of the best ways to save weight on auxiliary engines is to eliminate accessories and structures that are duplicated elsewhere in the airplane. In an effort to relieve the congestion behind main engines some airplanes are equipped with gear boxes. By designing into the gear box an auxiliary engine which would make use of the generator, oil pump, and fuel pump already required, considerable weight could be saved over batteries or conventional auxiliary power-plant installations. The auxiliary engine would drive only the necessary components of the gear box to keep friction at a minimum and would be operated when needed. During flight the auxiliary engine could be kept warm and ready for use by circulating gear-box oil through it.

Continuous-duty auxiliary power plants, like main engines, require supercharging. With a slight increase in the size of the main engine supercharger

The Computation of Magnetic Field Strength of Round Coils of Small Cross Section

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WHEN a solenoid, or cylindrical coil, has no iron core, the magnetic field in the space around it or within it may be computed very precisely. Formulas have been published[1] which take account of the length and thickness of the coil by algebraic terms which have been obtained by an integration over the cross section of the coil. This requires a calculation to be made twice, once for the end of the coil nearer the point for which the magnetic field is being computed, and once for the farther end. When the distance to this point is large compared with the dimensions of the coil section, the final computed result is a small difference of two larger quantities, resulting in a loss of accuracy. In this paper formulas for the magnetic field strength are presented which not only preserve the accuracy of computation, in such cases where they are applicable, but also save a considerable amount of work.

When the length and thickness of the solenoid or coil are so small compared with the radius that they may be neglected, formulas are available which give the field strength directly in terms of elliptic integrals[2] or of a series of zonal harmonics (see equations 12 to 24 of reference 1, and the earlier references given therein). These formulas can be used with actual coils by taking the position and diameter of the coil to be those of the circle at the middle of the coil section.

In this paper, correction formulas are given which can be used along with those of the preceding paragraph, and which will give the effect of the dimensions of the cross section. These formulas are in the form of convergent series, and, as is usual in such cases, their applicability is determined by the rapidity with which the terms become smaller and smaller, for any given problem. If in any case they are found inapplicable for this reason, recourse may be had to the more general set of formulas indicated by numbers in Figure 5, of reference 1.

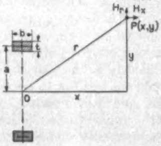

Figure 1. Circular coil

A general method by which correction terms for the section of a cylindrical coil may be derived is given in "Electricity and Magnetism" by J. C. Maxwell, volume 2, para. 700. If the coil section, which is a rectangle of length b and radial dimension t, be plotted with the middle point of the section as origin, then the magnetic field due to any small circular wire through the point (u, v) may be called H_{uv}. Expressing this as H_o and a series of its derivatives, and integrating from $-t/2$ to $t/2$ and from $-b/2$ to $b/2$, there results

$$H_{\text{coil}} = H_o + \Delta H_o$$

H_o is the magnetic field strength due to the single turn at the center of the cross section, the entire current of the cross section being taken to be concentrated in this turn. H_o may be computed by any of the formulas for single circular turns described in the preceding paragraphs.

$$\Delta H_o = \frac{1}{1!3!2^2}\left(t^2\frac{\partial^2 H_o}{\partial a^2} + b^2\frac{\partial^2 H_o}{\partial x^2}\right) + \frac{1}{1!5!2^4}\times$$
$$\left(t^4\frac{\partial^4 H_o}{\partial a^4} + b^4\frac{\partial^4 H_o}{\partial x^4}\right) + \frac{1}{3!3!2^4}t^2b^2\frac{\partial^4 H_o}{\partial a^2 \partial x^2} + \ldots \quad (1)$$

A case in which the method of this paper is very applicable is where the point $P(x, y)$ (see Figure 1), at which the value of the magnetic field strength is desired, is at a large distance from the coil.

A formula for the axial component of the magnetic field for this case is equation 20, reference 1, as follows:

$$H_{xo} = \frac{2\pi NI}{10r}\left[\frac{a^2}{r^2}P_2\left(\frac{x}{r}\right) - \frac{3}{2}\frac{a^4}{r^4}P_4\left(\frac{x}{r}\right) + \right.$$
$$\left.\frac{3\cdot 5}{2\cdot 4}\frac{a^6}{r^6}P_6\left(\frac{x}{r}\right) - \frac{3\cdot 5\cdot 7}{2\cdot 4\cdot 6}\frac{a^8}{r^8}P_8\left(\frac{x}{r}\right) + \ldots\right] \quad (2)$$

By applying equation 1, there is obtained

$$\Delta H_{xo} = \frac{2\pi NI}{10r}\left[C_{(2)}\frac{a^2}{r^2}P_2\left(\frac{x}{r}\right) - \right.$$
$$C_{(4)}\frac{3}{2}\frac{a^4}{r^4}P_4\left(\frac{x}{r}\right) + C_{(6)}\frac{3\cdot 5}{2\cdot 4}\frac{a^6}{r^6}P_6\left(\frac{x}{r}\right) -$$
$$\left. C_{(8)}\frac{3\cdot 5\cdot 7}{2\cdot 4\cdot 6}\frac{a^8}{r^8}P_8\left(\frac{x}{r}\right) + \ldots\right] \quad (3)$$

where

$$C_{(2)} = \frac{t^2}{12a^2}$$

$$C_{(4)} = \left(\frac{t^2}{2a^2} - \frac{b^2}{3a^2} + \frac{t^4}{80a^4} - \frac{t^2b^2}{36a^4}\right)$$

$$C_{(6)} = \left(\frac{5}{4}\frac{t^2}{a^2} - \frac{b^2}{a^2} + \frac{3}{16}\frac{t^4}{a^4} - \frac{t^2b^2}{2a^4} + \frac{b^4}{10a^4} + \ldots\right)$$

$$C_{(8)} = \left(\frac{7}{3}\frac{t^2}{a^2} - \frac{2b^2}{a^2} + \frac{7}{8}\frac{t^4}{a^4} - \frac{5}{2}\frac{t^2b^2}{a^4} + \right.$$
$$\left.\frac{3}{5}\frac{b^4}{a^4} + \ldots\right)$$

Similarly, the radial component at the point $P(x, y)$ is, by equation 21, reference 1,

$$H_{ro} = \frac{\pi NIy}{10r^3}\left[\frac{a^2}{r^2}P_2'\left(\frac{x}{r}\right) - \frac{3}{4}\frac{a^4}{r^4}P_4'\left(\frac{x}{r}\right) + \right.$$
$$\left.\frac{3\cdot 5}{4\cdot 6}\frac{a^6}{r^6}P_6'\left(\frac{x}{r}\right) - \frac{3\cdot 5\cdot 7}{4\cdot 6\cdot 8}\frac{a^8}{r^8}P_8'\left(\frac{x}{r}\right) + \ldots\right] \quad (4)$$

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enough air could be supplied for the auxiliary power plant more efficiently than from a separate supercharger built into the auxiliary engine. This has been done with good results.

Another source of "cheap" supercharging may be found in the exhaust from cabin supercharging. The auxiliary power plant could be mounted inside or outside the pressurized cabin. However, mounting it inside the cabin would be better from the standpoint of heat dissipation.

Auxiliary-engine accessories such as magnetos, fuel pumps, and carburetors can stand considerable improvement from the weight standpoint. Such equipment on most auxiliary engines is capable of serving engines many times larger.

Although auxiliary power plants necessarily are operating within the range of diminishing returns from the standpoint of weight and efficiency they reopen the avenues of development to designers of electrical equipment. Co-operation along the lines mentioned can result in better and lighter accessory power systems.

The same coefficients are used for each term as in equation 3; thus, by equation 1,

$$\Delta H_{r0}=\frac{\pi NIy}{10r^2}\left[C_{(2)}\frac{a^2}{r^2}P_2'\left(\frac{x}{r}\right)-\right.$$
$$C_{(4)}\frac{3}{4}\frac{a^4}{r^4}P_4'\left(\frac{x}{r}\right)+C_{(6)}\frac{3\cdot5}{4\cdot6}\frac{a^6}{r^6}P_6'\left(\frac{x}{r}\right)-$$
$$\left.C_{(8)}\frac{3\cdot5\cdot7}{4\cdot6\cdot8}\frac{a^8}{r^8}P_8'\left(\frac{x}{r}\right)+\ldots\right] \quad (5)$$

In the above formulas, the magnetic field is given at the point (x, y), the center of the round coil being the origin of co-ordinates and the axis of the coil being the axis of x. See Figure 1. The quantity $r^2=x^2+y^2$. The coil has a mean radius of a centimeters. Its cross section is rectangular, the radial thickness being t and the axial length, b. It is evident from the form of equations 3 and 5 that t and b should be small compared with the radius a for these formulas to be applicable. Dimensions are in centimeters. The dots represent multiplication.

$P_n(\mu)$ is a surface zonal harmonic. It may be computed by standard formulas[3] or the required values may be taken from tables.[4] $P_n'(\mu)=\frac{d}{d\mu}P_n(\mu)$.

Since these formulas are to be used when there is no iron or magnetic material near the coil, the flux density at the point $P(x, y)$, in lines per square centimeter, is numerically equal to the value of H given by the formulas.

Example 1. For a numerical example, take $a=10$ centimeters, $t=0$, $b=5$ centimeters, $x=24$ centimeters, and $y=32$ centimeters. See Figure 1. Then:

$r^2=1{,}600$, $r=40$, and $x/r=0.6$

By equation 2,

$$H_{x0}=\frac{2\pi NI}{10^4a}\times\frac{10}{40}(2.500+2.391+0.079-$$
$$0.007\ldots)=\frac{2\pi NI}{10^4a}\times1.241$$

$$\Delta H_{x0}=\frac{2\pi NI}{10^4a}\times\frac{10}{40}\left(-2.391\times\frac{1}{12}-0.079\times\right.$$
$$\left.\frac{39}{160}+0.007\times\frac{37}{80}\ldots\right)=\frac{-2\pi NI}{10^4a}\times0.054$$

$$H_x=H_{x0}+\Delta H_{x0}=\frac{2\pi NI}{10^4a}\times1.187$$

An alternative way of computing this result is by two applications of equation 39, reference 1. First, with $t=0$, by equation 39,

$$H_{x_1}=H_{x\infty}-\frac{\pi nI}{10}\left[\frac{a^2}{r_1^2}P_1\left(\frac{x_1}{r_1}\right)-\frac{3}{4}\frac{a^4}{r_1^4}P_3\left(\frac{x_1}{r_1}\right)+\right.$$
$$\left.\frac{3\cdot5}{4\cdot6}\frac{a^6}{r_1^6}P_5\left(\frac{x_1}{r_1}\right)-\frac{3\cdot5\cdot7}{4\cdot6\cdot8}\frac{a^8}{r_1^8}P_7\left(\frac{x_1}{r_1}\right)+\ldots\right]$$

where

$$H_{x\infty}=\frac{2\pi nI}{10}$$

$$n=\frac{N}{b}=\text{turns per centimeter}$$

$$r_1^2=x_1^2+y^2$$

$$x_1=x-\frac{b}{2}=24-2.5=21.5 \text{ centimeters}$$

$$H_{x_1}=\frac{\pi NI}{10a}\times\frac{a}{b}[2-0.037523-0.001368+$$
$$0.0000090+0.0000034+0.0000001\ldots]$$
$$=\frac{\pi NI}{10a}\times\frac{10}{5}\times1.961121$$

For

$$x_2=x+\frac{b}{2}=24+2.5=26.5,$$

$$H_{x_2}=\frac{\pi NI}{10a}\times\frac{10}{5}\times1.962307$$

$$H_x=H_{x_2}-H_{x_1}=\frac{2\pi NI}{10a}(1.962307-$$
$$1.961121)=\frac{2\pi NI}{10^4a}\times1.186$$

This agrees very well with the result by means of ΔH_0. The method by equation 39 takes about twice as long as equations 2 and 3, for this example. It has the advantage that further terms can be computed indefinitely if desired.

For the same shape of coil as has been taken up, namely, with t and b small compared with the radius a, the magnetic field strength near the center of the coil can be computed by means of equation 1, by applying it to equations 17 and 19 of reference 1.

By equation 17,

$$H_{x0}=\frac{2\pi NI}{10a}\left[1-\frac{3}{2}\frac{r^2}{a^2}P_2\left(\frac{x}{r}\right)+\right.$$
$$\left.\frac{3\cdot5}{2\cdot4}\frac{r^4}{a^4}P_4\left(\frac{x}{r}\right)-\frac{3\cdot5\cdot7}{2\cdot4\cdot6}\frac{r^6}{a^6}P_6\left(\frac{x}{r}\right)+\ldots\right] \quad (6)$$

By equation 1,

$$\Delta H_{x0}=\frac{2\pi NI}{10a}\left[D_{(0)}-D_{(2)}\frac{3}{2}\frac{r^2}{a^2}P_2\left(\frac{x}{r}\right)+\right.$$
$$D_{(4)}\frac{3\cdot5}{2\cdot4}\frac{r^4}{a^4}P_4\left(\frac{x}{r}\right)-D_{(6)}\frac{3\cdot5\cdot7}{2\cdot4\cdot6}\frac{r^6}{a^6}\times$$
$$\left.P_6\left(\frac{x}{r}\right)+\ldots\right] \quad (7)$$

where

$$D_{(0)}=\left(\frac{t^2}{12a^2}-\frac{b^2}{8a^2}+\frac{t^4}{80a^4}-\frac{t^2b^2}{16a^4}+\frac{3}{128}\frac{b^4}{a^4}\ldots\right)$$

$$D_{(2)}=\left(\frac{t^2}{2a^2}-\frac{5}{8}\frac{b^2}{a^2}+\frac{3}{16}\frac{t^4}{a^4}-\frac{25}{32}\frac{t^2b^2}{a^4}+\right.$$
$$\left.\frac{35}{128}\frac{b^4}{a^4}\ldots\right)$$

$$D_{(4)}=\left(\frac{5}{4}\frac{t^2}{a^2}-\frac{35}{24}\frac{b^2}{a^2}+\frac{7}{8}\frac{t^4}{a^4}-\frac{245}{72}\frac{t^2b^2}{a^4}+\right.$$
$$\left.\frac{147}{128}\frac{b^4}{a^4}\ldots\right)$$

$$D_{(6)}=\left(\frac{7}{3}\frac{t^2}{a^2}-\frac{21}{8}\frac{b^2}{a^2}+\frac{21}{8}\frac{t^4}{a^4}-\right.$$
$$\left.\frac{315}{32}\frac{t^2b^2}{a^4}+\frac{2{,}079}{640}\frac{b^4}{a^4}\ldots\right)$$

For the radial component of the field near the coil center, by equation 19, reference 1,

$$H_{r0}=\frac{\pi NIyr}{10a^3}\left[P_2'\left(\frac{x}{r}\right)-\frac{3}{4}\frac{r^2}{a^2}P_4'\left(\frac{x}{r}\right)+\right.$$
$$\left.\frac{3\cdot5}{4\cdot6}\frac{r^4}{a^4}P_6'\left(\frac{x}{r}\right)-\ldots\right] \quad (8)$$

and, by equation 1,

$$\Delta H_{r0}=\frac{\pi NIyr}{10a^3}\left[D_{(2)}P_2'\left(\frac{x}{r}\right)-\right.$$
$$D_{(4)}\frac{3}{4}\frac{r^2}{a^2}P_4'\left(\frac{x}{r}\right)+D_{(6)}\frac{3\cdot5}{4\cdot6}\frac{r^4}{a^4}\times$$
$$\left.P_6'\left(\frac{x}{r}\right)-\ldots\right] \quad (9)$$

where $r^2=x^2+y^2$ and where the coefficients $D_{(2)}$, $D_{(4)}$, and $D_{(6)}$ are given after equation 7.

The results described in this paper were derived in connection with a thesis at Massachusetts Institute of Technology.

Formulas for the first few surface zonal harmonics are:

$P_1(\mu)=\mu \qquad P_2(\mu)={}^1/_2(3\mu^2-1)$
$P_3(\mu)={}^1/_2(5\mu^3-3\mu)$

$$P_4(\mu)=\frac{1}{2\cdot4}(5\cdot7\mu^4-2\,3\cdot5\mu^2+1.3)$$

$$P_5(\mu)=\frac{1}{2\cdot4}(7\cdot9\mu^5-2\cdot5\cdot7\mu^3+3\cdot5\mu)$$

$$P_6(\mu)=\frac{1}{2\cdot4\cdot6}(7\cdot9\cdot11\mu^6-3\cdot5\cdot7\cdot9\mu^4+$$
$$3\cdot3\cdot5\cdot7\mu^2-1\cdot3\cdot5)$$

$$P_8(\mu)=\frac{1}{2\cdot4\cdot6\cdot8}(9\cdot11\cdot13\cdot15\mu^8-4\cdot7\cdot9\cdot11\cdot13\mu^6+$$
$$6\cdot5\cdot7\cdot9\cdot11\mu^4-4\cdot3\cdot5\cdot7\cdot9\mu^2+1\cdot3\cdot5\cdot7)$$

$P_1'(\mu)=1 \qquad P_2'(\mu)=3\mu$
$P_3'(\mu)={}^1/_2(3\cdot5\mu^2-1\cdot3)$
$P_4'(\mu)={}^1/_2(5\cdot7\mu^3-3\cdot5\mu)$

and so on, by simple differentiation of the formulas given above.

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Aircraft-Engine Torque Instruments

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Synopsis: Flight-operations experience has shown that significant improvements in fuel economy may be obtained through the use of aircraft-engine torque instruments as regular flight instruments. This paper describes the better-known types of torque instruments and introduces a new type that has recently come through its development stage.

THE performance of airplane engines in flight is evaluated ordinarily by means of engine speed, manifold pressures, air temperature, airplane speed, and flight altitude in terms based upon factory-calibrated engines. Corrections for fuel variations, atmospheric humidity, and actual engine condition are usually indeterminate and cannot be applied, so that substantial variations from the "chart" engine output frequently are encountered. Torque measuring instruments have been used when available for test flying of new aircraft designs and to a limited extent as regular flight instruments to reduce fuel consumption and extend the useful radius of flight operations. The value of a torque instrument on the propeller shaft of an airplane engine lies in a balance between the advantage of knowing true engine-power delivery and the disadvantage of additional weight, service difficulties, and increased equipment cost. For very long-range operations of large military aircraft, engine torque instruments or some accurate type of power-measuring devices are a necessity and a military requirement. As ranges are shortened and as airplane size decreases, excessive torque-instrument weight and cost increases can quickly outweigh their advantages.

General Types of Torque Instruments

There are two general types of torque instruments which have been applied to aircraft engines to determine engine output; these are the hydraulic and the electrical types. The hydraulic torque instrument[1,2] is the better known of the two and has been in use for a number of years; it usually measures the torque reaction on a stationary member of the propeller-shaft reduction-gear train by means of an hydraulic piston and a pressure-regulated oil supply. The oil pressure is controlled by means of slight piston movements to balance exactly the torque reaction of the selected gear, usually the stationary sun gear of a planetary gear set, Figure 1, and the oil pressure which is proportional to the torque is measured by a conventional pressure gauge. When double-reduction gears are required, or when operation at an extremely low temperature is necessary, and when excessive torsional vibrations are present, the hydraulic system becomes difficult to handle. Electric torque instruments fall into three general classifications, the strain-resistive, strain-capacitive, and strain-magnetic.

Electrical-Resistance Strain Gauges

The strain-resistive type may measure either the twist in the propeller shaft by means of strain-sensitive resistance elements mounted directly on the shaft, or it may measure the deflections of a stationary reaction member, somewhat similar to the conventional hydraulic torque instrument in application except that the actual deflection of a spring is measured rather than the balancing oil pressure. If the strain-sensitive resistance elements are attached to a stationary reaction member, direct electric connections can be made between the resistance units and the indicating instrument; but if the resistance units are mounted on a rotating shaft so as to be sensitive to the actual twist in a specified gauge length of shaft, electric connections usually must be made through slip rings on the shaft with brushes riding on the slip rings, Figure 2. Attempts have been made to use carbon-pile resistance elements in such torque instrument or strain-gauge applications because of the high energy levels which can be tolerated in carbon piles. This should make possible direct indication of torque on conventional ammeters or voltmeters without using amplifiers; however, the strain-resistance characteristics of carbon piles have not been sufficiently stable and subject to manufacturing control within required accuracy limits to permit their use. Wire-resistance strain gauges[3] have the required accuracy, but the resistance changes for strains corresponding to maximum allowable stresses in both the engine parts and the wire-resistance elements seldom exceed one per cent of the initial resistance. Therefore, use of a sensitive potentiometer and balancing bridge is required with wire-resistance strain gauges, or high-gain electric amplifiers must be used with direct-indicating instruments. The ease of application of the small resistance-wire strain gauge and its general reliability makes it quite valuable for laboratory work, but its low sensitivity appears to rule it out as an aircraft flight instrument. A further disadvantage is the relatively large temperature coefficient of resistance of most of the wire-resistance strain gauges; normal temperature excursions in aircraft engines result in temperature-resistance changes of greater magnitude than the strain-resistance changes under measurement; these temperature effects can be eliminated, however, by careful adjustment of compensators.

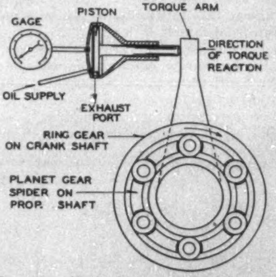

Figure 1. Hydraulic torque instrument applied to sun gear of planetary transmission

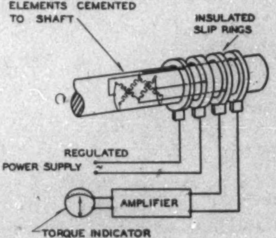

Figure 2. Torque instrument using resistance-wire strain gauges attached to shaft

Electrical-Capacitance Strain Gauges

Strain-capacitive elements are used in much the same manner as strain-resistive elements, and may be mounted either on rotating shafts with slip-ring connections or on torque-responsive stationary members of the engine. Here an air-dielectric capacitor is formed of two groups of plates or conducting surfaces insulated from each other and so positioned that, when the assembly is subjected to strain, either dielectric spacing between plates of opposite polarity changes or opposed plate areas

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alter in proportion to the strain, with a resultant change in the capacitance of the capacitor. The per cent change in capacitance which can be obtained for permissible strains in engine parts can be large, and for that reason much effort has been expended in attempts to utilize strain-capacitive elements in strain gauges and torque instruments. However, space and weight limitations usually restrict the capacitance of the strain-sensitive element to a few micromicrofarads; and therefore, even with a-c power supplies of several hundred kilocycles frequency, energy levels in the sensitive circuit elements are so low as to require amplifiers ahead of the torque indicating instrument, with the usually attendant difficulties of maintaining calibration. Capacitance-sensitive strain elements must also be hermetically sealed to exclude oil, moisture, and dirt; and the uncertain stray capacitances of electric connections and mechanical supports are also major difficulties.

Electromagnetic Strain Gauges

Strain-magnetic elements lend themselves to the measurement of small displacements much more readily than do any of the aforementioned strain-sensitive devices when it is desirable to connect direct-indicating instruments to the strain-sensitive circuit without interposing amplifiers. The conventional magnetic strain gauges operates at sufficiently high energy levels that its output can be utilized to drive an indicating instrument directly; and in such manner it is commonly used as the sensitive element in many precision gauges or comparators as well as for aircraft-engine testing.[4]

It is usually small and operates on conventional a-c supply circuits, 60 cycles in the case of ground equipment and 400 cycles for air-borne equipment, and can take any one of several different forms; the one in Figure 3 with its connection diagram shown in Figure 4 is simple and commonly used. It consists of two iron-core inductance coils with variable-length air gaps in their magnetic paths; moving the armature A of Figure 4 to the left will decrease the air gap of coil Z_1, increasing its inductance, and at the same time will increase the air gap of coil Z_2 with a corresponding decrease of its inductance. Since Z_1 and Z_2 are both elements of a four-arm bridge, the output of the bridge indicated by the instrument M will be almost exactly proportional to the physical displacement of the armature A from its center position, within the useful limits of the strain gauge. The other two bridge arms, Z_3 and Z_4, are usually made part of a center-tapped autotransformer, which decreases the bridge impedance and thus increases the maximum signal energy available.

Magnetic strain gauges can be mounted in the take-off head of the conventional hydraulic torque instrument, replacing the hydraulic piston and pressure gauge. When this is done, a spring element, usually a short section of steel tubing, resists the reaction of the sun-gear torque arm and the strain gauge measures the deflections of the steel tube, Figure 5. They also have been mounted directly upon the propeller shaft of an engine or reduction-gear assembly, Figure 6, with electric connections made through slip rings on the shaft.

The Magnetic-Coupled Torquemeter

For shaft mounting, the slip-ring design is likely to be difficult from the standpoints of service trouble and contact resistance. On the other hand, there are numerous aircraft-engine applications for torque instruments where it is desirable if not imperative to measure the twist in a shaft when the engine output is to be known with any degree of accuracy. The Magnetic-Coupled Torquemeter therefore was designed to eliminate slip rings and still retain a shaft-mounted strain gauge. A schematic illustration appears in Figure 7, and a picture of a typical assembly is reproduced in Figure 8. If we refer to Figure 7, the shaft is provided with three flanges on which are mounted three toothed rings of magnetic material, but separated from contact with the steel shaft by means of nonmagnetic spacers made of a metal such as bronze or brass. Overlapping teeth or projections from each of the three magnetic rings form two sets of air gaps, one set between outer ring 1 and the middle ring A, and one set between outer ring 2 and the middle ring A. When the shaft is loaded or twisted in a clockwise direction, the air gaps between teeth on rings 1 and A are shortened and the air gaps between teeth on rings 2 and A are lengthened; this is in effect the same as moving the armature A of Figure 4 to the left.

Magnetic flux flows across the two sets

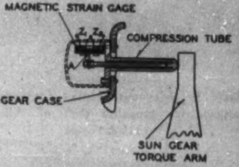

Figure 5. Magnetic strain gauge applied to sun gear of planetary transmission

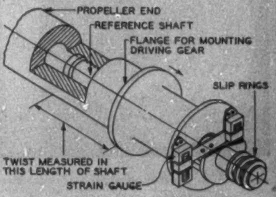

Figure 6. Magnetic strain gauges measuring the twist of a propeller shaft

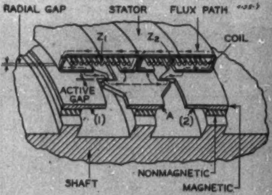

Figure 7. Schematic Illustration of Magnetic-Coupled Torquemeter

Figure 3. Magnetic strain gauge

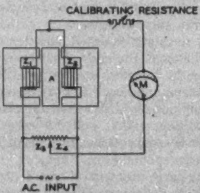

Figure 4. Simplified magnetic-strain-gauge circuit

Figure 8. Small Magnetic-Coupled Torquemeter for test-floor use

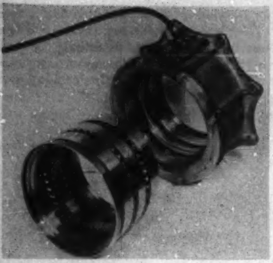

Figure 9. Magnetic-Coupled Torquemeter designed for propeller shaft of Allison V-1710-E engine

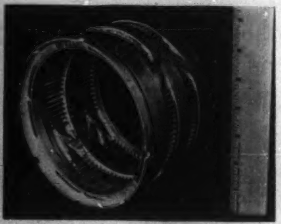

Figure 10. Magnetic-Coupled Torquemeter rotor with four tooth gaps per side

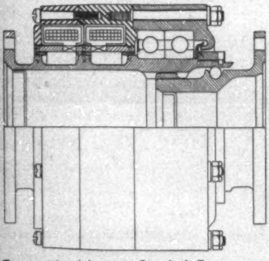

Figure 11. Magnetic-Coupled Torquemeter designed for the engine extension shaft of an Allison engine

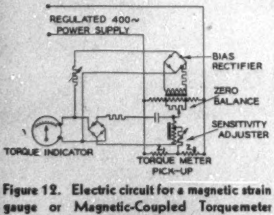

Figure 12. Electric circuit for a magnetic strain gauge or Magnetic-Coupled Torquemeter using a regulated power supply

of air gaps, induced by the two stationary coils Z_1 and Z_2, also corresponding to Z_1 and Z_2 of Figure 4. Coils Z_1 and Z_2 are shown completely encircling the shaft assembly and the magnetic return paths are through their encasing shells of magnetic material and across the radial air gaps between the stationary-coil assembly and the rotating-shaft assembly. These air gaps have relatively large effective areas and short lengths so that their magnetic permeances are quite large compared with the permeances of the active air gaps between the toothed rings; consequently, the insertion of the radial air gaps in the magnetic circuit has but small effect upon the characteristics of the strain gauge. Normal eccentric movements of the rotating shaft in its bearings relative to the stator cause second-order changes in permeance of the radial air gaps and finally result in only third-order changes in torque-instrument calibration.

When strain-gauge elements are mounted on a rotating shaft, precautions must be taken to avoid errors due to bending moments and compression or tension loads on the shaft so that only pure torque is measured. This requires that a balanced magnetic circuit be used with both Z_1 and Z_2 responsive to shaft torque loads, and it also requires that the sensitive air gaps be distributed uniformly around the circumference of the shaft. Slip-ring installations of two conventional magnetic strain gauges diametrically opposite each other on a shaft and with their coil circuits either connected in series or in parallel have shown a satisfactory lack of response to bending loads on the shaft where moderate loads are imposed. Some aircraft-engine propeller shafts are subjected to bending moments from propeller gyroscopic couples in spins that may result in maximum fiber stresses of the order of 100,000 pounds per square inch where normal stress in torsion is of the order of 8,000 to 16,000 pounds per square inch.

Well-distributed sets of sensitive air gaps are desirable in such circumstances, and Figure 9 shows a rotor for a magnetic-coupled torque instrument with a large number of small individual air gaps in each set around the shaft. This particular shaft element is intended to be mounted between two existing flanges on an engine propeller shaft, and therefore, in order to divide circumferential strain due to twist in the shaft length between flanges equally between the two sets of air gaps, three rotor rings are mounted on a torsionally flexible cage, the ends of which are permanently fastened to the two flanges. The stator carrying the two coils and their magnetic shells is also shown in the same illustration, and is bolted to the inner-thrust-bearing retainer ring inside of the propeller reduction-gear housing.

The large number of small air gaps shown in the rotor of Figure 9 is very effective in averaging out bending and thrust loads on the propeller shaft, but does lead to excessive magnetic leakage around the teeth and also to increased manufacturing difficulties since normal air-gap spacing is of the order of 0.004 to 0.010 inch, depending upon shaft stiffness characteristics. Another rotor for the same application is shown in Figure 10 in which only four air gaps each of relatively large area are used per sensitive gap set; there is a considerable increase in apparent sensitivity of this torque instrument over the one shown in Figure 9, due to the better magnetic design and the large reduction in leakage flux around the rotor. Flux leakage at 400 cycles per second to the steel shaft is effectively reduced to a negligible quantity if the nonmagnetic spacer rings between the toothed rings and the shaft are made of high-conductivity materials such as brass, bronze, or copper.

Figure 11 shows another magnetic-coupled torque instrument intended for use on the extension shaft between the engine and a separate propeller reduction-gear box. The three toothed magnetic rings are mounted on three shaft flanges instead of on a flexible cage, and the short shaft section carrying the rings is inserted in the extension-shaft drive with flanged or splined couplings. A better mechanical job is obtained when it is possible to mount the toothed rings directly upon individual flanges integral with the shaft rather than upon the surface of a flexible cage or sleeve.

Temperature Effects in Magnetic Strain Gauges

The effects of ambient-temperature changes upon magnetic strain gauges can be divided into those originating in the instrument circuit and those originating in the shaft pickup unit. Temperature effects in the instrument circuit are readily corrected by conventional means and attention to good design practice. The temperature effects in the shaft element are more difficult to handle ordinarily, particularly since temperatures may range from −50 degrees centigrade to hot engine oil temperatures. The changes in modulus of elasticity of the shaft material are usually small over the normal temperature ranges, but serious unbalances of the impedance bridge with large shifts in the zero adjustment of the torque instrument may result from large ambient-temperature changes around the shaft pickup unit; this may be particularly noticeable when large temperature differentials appear from one end of the shaft unit to the other. These calibration changes can be held to small limits if sufficient care is used in design to prevent large hysteresis and eddy-current losses in the magnetic parts of the shaft unit. High power factors in the impedance coils of the strain-gauge bridge usually are the result of abnormal magnetic losses due to saturation effects in parts of the circuit;

and such losses are usually highly temperature-sensitive, so that torque-instrument accuracy suffers from temperature changes. By exercising care in design and application, these temperature effects can be held within acceptable limits; one of the early Magnetic-Coupled Torquemeters that showed excessive temperature sensitivity was corrected by adding a small resistor with a high temperature coefficient of resistivity in series in one of the impedance coil arms of the bridge. A nickel-wire resistor was used and fastened to the shaft pickup stator housing.

Magnetic-Strain-Gauge Instrument Circuits

A number of precautions must be observed in applying magnetic strain gauges or Magnetic-Coupled Torquemeters to aircraft instrumentation problems. The a-c power supplies ordinarily available on airplanes may be subject to rather wide variations in voltage and frequency; and, while close frequency regulation is not essential, the calibration of the torque instruments is proportional to the supply voltage when a simple circuit such as shown in Figure 4 is used. Therefore a voltage-regulated power supply is essential to accurate calibration under these circumstances. Since the electrical load is small and fixed, the voltage-regulator problem is not particularly difficult. The power requirements of a magnetic strain gauge at 400 cycles is about five voltamperes. The Magnetic-Coupled Torquemeter, due to greater iron loss and flux leakage requires 30 to 50 volt-amperes. Ordinary frequency variations are compensated for, when compensation is required, by the use of a small capacitor in the instrument circuit. On airplanes where the only available electric power is direct current, it is possible to use a small inverter to obtain the necessary 400 cycle power. Various regulators, such as the carbon-pile, saturated-reactor, and centrifugal speed-governing type are available. By designing a unit specially for the job, it is possible to reduce the weight of the inverter, complete with regulator, to about two pounds per torque instrument on a two-engine airplane.

The indicating instrument is usually a rectifier-type milliammeter, and the complete bridge output circuit must include the proper external resistances to insure that the instrument circuit will provide accurate voltage measurements of the strain-gauge bridge output over wide ranges of ambient temperature, input voltage, and frequency. Such a circuit is shown in Figure 12, which is Figure 4 with the added refinements of a voltage regulator, frequency-compensating capacitor, sensitivity-adjusting rheostat, zero-balance potentiometer, and a small bias rectifier. The bias rectifier is added, because a true null balance of the bridge is never obtainable because of the presence of phase unbalance and harmonics in the supply voltage. Therefore, without a biasing rectifier or an off-scale zero spring adjustment, the indicator pointer could never be brought to zero.

The use of a d-c ratio instrument instead of a conventional milliammeter eliminates the necessity for the voltage regulator, and since frequency variations are relatively unimportant in magnetic-strain-gauge instrument errors, its use has many attractive features. A d-c ratio instrument consists of a permanent-magnet instrument movement with two coils set at a wide angle to each other on the shaft of the moving element which carries the pointer. Connections are made to the moving coils through soft gold conducting spirals instead of the conventional springs, and the resultant spring restraint is negligible under normal conditions. When each coil is made to carry current, the position which the pointer will assume is determined by the resultant magnetic field of the two coils, and the instrument scale is marked as the ratio of current flowing in the "forward" coil to that flowing in the "reverse" coil. The torque-instrument application of a d-c ratio instrument is shown in Figure 13, and it should be noted that the coils are energized through instrument-type rectifiers and also that a biasing voltage is required for the forward coil for zero-adjustment purposes. Unfortunately, the scale length of present d-c ratio instruments is limited to that of a 90 degree instrument movement, and the scale is nonlinear. A comparison of the short-scale ratio instrument with a 270-degree-scale-movement aircraft-type milliammeter such as is used in the circuit of Figure 12 is shown in Figure 14.

A greatly simplified torque instrument utilizing the magnetic strain gauge working directly into an a-c ratio instrument is shown in the connection diagram of Figure 15, in which voltage regulators, rectifiers, and all biasing circuits are eliminated. The stationary coil of the instrument is energized directly from the power-supply source, and the moving coil is connected to the bridge output. Current is conducted to the moving coil through soft gold spirals with a minimum of spring restraint, and the moving element comes to rest when the inphase component of the voltage induced in the coil is equal to and opposes the bridge output voltage. Since the power factor of the stationary coil is usually quite low, the out-of-phase voltages induced in the moving coil are small and have a negligible effect upon accuracy. Physically the a-c ratio-instrument movement resembles the conventional frequency-instrument movement. Close coupling between the moving coil and the stationary coil is desirable in order to reduce power requirements, and a laminated silicon steel or Hypernik magnetic structure is used. Long-scale or 270-degree a-c instrument movements have not yet been developed for aircraft use, and the a-c ratio-instrument airplane-engine torque instruments probably will have to wait until such movements are available in production. Figure 16 shows an experimental long-scale a-c ratio-instrument movement for aircraft use compared with a modified wattmeter move-

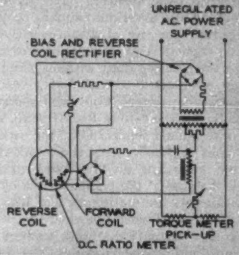

Figure 13. Electric circuit for a magnetic strain gauge or Magnetic-Coupled Torquemeter using an unregulated power supply and a d-c ratio instrument

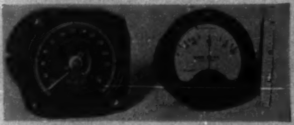

Figure 14. Aircraft-type d-c milliammeter (left) and d-c ratio instrument (right)

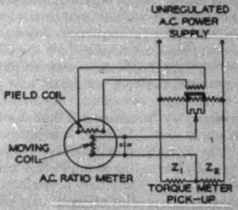

Figure 15. Electric circuit for a magnetic strain gauge using an a-c ratio instrument

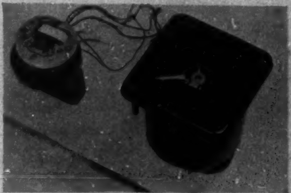

Figure 16. A-c ratio instruments

Experimental movement at left, modified wattmeter at right ment originally designed for marine use and therefore somewhat large and heavy for aircraft applications; both instrument movements operate successfully as a-c ratio instruments.

Other Shaft-Mounted Types Without Slip Rings

Several experimenters have made attempts to utilize various coupling schemes to permit the use of strain-resistive or strain-capacitive gauges mounted directly upon the rotating shaft without using slip rings or electric connections. Strain-sensitive capacitors have been mounted directly on the torque loaded shaft with their terminals connected to the secondary coils of transformers, these coils completely encircling the shaft and rotating with the shaft; the primary coils of the transformers are stationary and also encircle the shaft assemblies and the rotating coils. Mutual inductance between the primary and secondary coils of the rotating transformer thus formed constitutes the link between the stationary instrument circuit elements and the strain-sensitive capacitor on the loaded shaft.

Coils also have been placed close to or encircling shafts to cause magnetic flux to flow in the surface of the shaft itself or through special surface coatings on the shaft, so that, when the shaft is twisted under torque loads and the magnetic permeability and electrical resistivity of the shaft change, such changes are reflected as changes in impedance of the flux-inducing coil or coils.[5] This magnetic skin-effect method of torque measurement is sensitive to temperature, shaft bending, mechanical and magnetic hysteresis, and thrust loads.

Relatively high-frequency (kilocycles) power supplies are required for the aforementioned two schemes, and amplifiers are required between the shaft unit and the indicating instrument. The magnetic skin-effect or magnetostriction torque-instrument shaft element approaches the ultimate in design simplicity; and if means of eliminating temperature errors and hysteresis effects can be found, the simplicity of the shaft element may offset the disadvantages and difficulties of high-frequency power supplies and amplifiers.

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High-Voltage Compressed-Gas Power Transformers

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FELLOW AIEE

IN a previous article,[1] the author pointed out the many advantages of high-voltage power transmission in compressed gas. Compressed CO_2 at the moderate pressure of 40 atmospheres (absolute) has a dielectric strength about 20 times as high as that of air at normal atmospheric pressure, and compressed SO_2 at 40 atmospheres about 30 times as high, so that the line conductors need be separated by only a few inches instead of the customary many feet and may, therefore, be enclosed in a reasonably small pipe line. This system offers opportunities for many important cumulative economies in the terminal equipment also, and this paper is devoted to their discussion. It is shown that the use of compressed gas, instead of oil, as the insulating and cooling fluid makes possible power transformers that are only about one fourth as large as the present ones for the same duty, are for all intents and purposes nonaging, and are highly resistant to damage from internal arc-overs, besides many other advantages.

While such transformers have not as yet been built, and further laboratory research is desirable on certain phases of the system, yet, based on the great economies shown by several exploratory designs listed in Table II, the author expects that the system will be adopted in the near future to transmit large amounts of power over great distances by transmission lines of very much higher voltages than heretofore and at frequencies as high as 600–1,200 cycles per second.

Important Facts Underlying the System

The more important facts from which the various advantages of the proposed transformer system arise are as follows:

1. The dielectric strength of some compressed gases is greater than that of oil.

2. Gases are not subject to cumulative breakdown to which solid insulation—the backbone of the present-day high-voltage transformers—is subject.

3. Gases have no dielectric loss and can, therefore, be operated at 600–1,000 cycles per second without any harm, making possible corresponding reductions in the sizes of the cores and also of the coils.

4. Unlike oil and the conventional solid insulations used in transformers, gases do not age and are not injured in any way by moderately high temperatures, and therefore the cores of compressed-gas transformers can be operated a couple of hundred degrees centigrade hotter than the windings and the windings a hundred degrees hotter than now to transform much larger kilovolt-amperes per pound than heretofore.

5. For heat transfer, compressed gases are many times more efficient than atmospheric air used in the conventional shell-type air-blast transformers.

These and additional facts and considerations are discussed below in greater detail.

Compressed Gases Versus Oil

Table I shows the leading data on the 60-cycle dielectric strength of several gases at various pressures and that of oil. It will be seen in the table that CO_2 at 40 atmospheres absolute has a 60-cycle strength of 1,650 volts per mil as against 750 volts per mil for oil—a ratio of 2 to 1 in favor of the compressed gas. For transient voltages of steep rise and short duration, such as the 1.5x40 standard impulse wave of the Institute, oil is credited with a strength of twice its 60-cycle value, whereas the strength of the gases is *assumed* to be unchanged. Compressed SO_2 at 40 atmospheres (2,400 volts per mil) is approximately 50 per cent higher than that of either nitrogen or CO_2. No data are now available on the impulse-strength ratios of compressed gases circulated at a high velocity. These may be higher than unity. Laboratory investigation of them, in both uniform and nonuniform fields, is very desirable.

Cumulative Failure of Solid Insulation Under Impulse Voltages

As impulse voltages are of rare occurrence and of very short duration, if a gas does not break down under the sudden stress, it probably will recover from the

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The author is indebted to Glenn B. Warren for suggesting the investigation of underground high-voltage transmission in compressed-gas-filled welded steel pipes, to H. H. Skilling for encouragement derived from his article (reference 3), to the many engineers and scientists mentioned in reference 1, and to the author's own associates.

This paper is speculative and looks toward the future; consequently, it contains many statements of opinion for which there are no supporting data or facts.

shock with its strength unimpaired. But with solid insulations a small permanent harm may be done without apparent failure, and this process will be cumulative. The overvoltage failure of a conventional oil-and-solid-insulated transformer will not take place necessarily at the first heavy surge occasioned by lightning, but after a number of such surges have produced minute but progressive and cumulative impairments. Then, a less severe surge may cause an outage.

Aging of Solid Insulation

It is well-known that the customary fibrous insulations used in oil-filled transformers age physically and become brittle at a rate twice as fast for every eight-degree-centigrade increase in temperature. Every substantial overload thus represents a substantial shortening of the useful life of the transformer. Such an effect does not exist in compressed-gas transformers, and therefore short-time overloads may be handled without any residual harm. Such transformers will be practically nonaging.

Problem of Nonuniform Fields in Gases and Solid Insulations

Uniform distribution of voltage stresses is, of course, desirable no matter what the insulation may be, but in transformers the compressed gases enjoy a particular advantage over solid insulations. In solid insulations overstress implies cumulative damage, and, no matter how small the damage may be per day, cumulation of such damage implies eventual failure. For this reason, in conventional apparatus, the bulk of the solid insulation is operated at a very low and uneconomical stress. A good example of this is the slot-lining insulations in generators and motors. The slot lining of a high-voltage stator winding may prove in the laboratory a strength of several thousand volts per mil in a uniform field and yet, based on experience, be operated at only 30–60 volts per mil on the average.

Because of the greater electronic mobilities in gases, it is wise to proceed at first with generous margins of safety in compressed-gas apparatus, until a sufficient amount of service experience is gained, but such margins of safety are more economically practicable with compressed gases than with oil and solid insulation.

Value of High Frequencies

One of the major economy factors in this system is the use of 600–1,200-cycle frequencies made possible by the replacement of oil and solid insulation by compressed gas. If compressed gas is used in combination with these higher frequencies, sizes drop to about a fourth of their present values.

The voltage produced by a given flux varies directly with the frequency. At tenfold frequency the voltage, and hence the kilovolt-amperes, is ten times as large for the same flux, or the flux required for the same kilovolt-amperes is only one tenth as large. Since, however, at constant flux density, core loss increases with frequency, the same high flux densities may not be used at tenfold frequency as at normal, and the full tenfold benefit may not be secured; yet, by virtue of the permissible high core temperatures and by the use of seven-mil sheet steel it is possible to use sufficiently high flux densities to afford the great economies indicated.

Table I. Dielectric Strength of Four Gases and Commercial Transformer Oil

Tested at 60 Cycles Between Two Spherical Electrodes of 2,500-Mils Diameter With a 100-Mil Gap

Gas Pressure in Atmospheres Absolute	Volts Per Mil (Crest)				
	Air	Nitrogen	CO_2	SO_2	Oil
1	85	95	75	200	750
20	1,300	1,000	1,000	2,000	
40	1,850	1,450	1,650	2.400	
60	2,100	1,550	2,250		

Departure from 60 cycles at this late date may appear to some as impractical, but the best economic course is to utilize 600 cycles (or more) for the major high-voltage transmission system and network of any single enormous territory, and employ frequency changers or rectifiers where the service demands a different frequency or direct current.

These higher frequencies reduce the size not only of the big terminal equipments but also of all power transformers used in the industry. Rectifier installations for electrometallurgical and electrochemical processes, as for the production of aluminum and magnesium, include already several millions of kilovolt-amperes in step-down power transformers. Rectifier applications (all requiring special step-down transformers) for railways, steel mills, mines, and shipyards are rapidly growing. An increase of 10 to 1 in frequency, with compressed-gas insulation, will reduce the size and cost of these transformers to about one seventh.

These higher frequencies are particularly advantageous in welding as to speed, quality, safety, and various other cumulative economies. The a-c arc is more stable and produces a better weld at the higher frequencies, and therefore greater speeds and lower voltages may be used. The combination of lower voltages and higher skin effect will greatly reduce shock hazards to the operator from accidental contact with live electrodes. Careful tests to confirm these conclusions ought to be undertaken as soon as possible.

Resistance-welding and flash-welding systems also will benefit as to speed, quality, and greatly reduced cost of equipment, from a tenfold increase in frequency.

Effect of High Core Temperatures on Efficiency and Economy

As far back as 1911 and 1912 MacLaren published his researches demonstrating substantial reductions in core loss at increasing core temperatures.[2] As core temperatures heretofore have been limited to about 90 degrees centigrade to avoid the sludging of oil, now, with the replacement of oil by compressed gases, it becomes possible to operate the transformer cores at temperatures as high as 300–325 degrees centigrade, so as to realize corresponding reductions in core loss and secure great economies in core material by operating the cores at higher frequencies and flux densities than now possible.

These high temperatures are particularly advantageous in the case of those compressed gases (as SO_2) which tend to liquefy at the lower temperatures.

Thermal Screening of Windings

In a compressed-gas design the windings also will be operated at higher temperatures (though not so high as the core temperatures), so as to enable them to carry greater loads per pound. Higher winding temperatures naturally tend to raise the I^2R, but in this system a higher iron-to-copper ratio would be maintained using relatively more flux and fewer turns, and thus the increase in the I^2R due to higher temperature will be small and may be offset by operating the core at a still higher temperature.

With core temperatures of the order of 300 degrees centigrade the winding temperatures should be 100 degrees less, in which case provision must be made for the thermal screening of the windings from the core. Excellent high-temperature materials are now plentifully available for turn and layer insulation and for spacers and strengthening supports, and for screening the windings from the core. Mycalex, asbestos, lava compositions, or ceramic materials in suitable thicknesses easily will withstand a temperature drop of 100 degrees centigrade between core and windings.

Heat-Transfer Procedure

The stream of circulating gas will be heated by the transformer losses as it ascends from the bottom to the top of the transformer. Above the transformer, or in the upper portions of the tank, condensation may be effected by providing a

Table II. Five Exploratory Compressed-Gas Designs for Power Transformers

	A	B	C	D	E
Rated output (in kilovolt-amperes)	20,000	20,000	20,000	50,000	100,000
Periodicity, N (cycles per second)	60	600	1,200	1,200	1,200
Primary voltage (kilovolts)	80	80	80	200	345
Primary turns (T)	240	160	132	180	200
Magnetic flux (M) (megalines)	126	18.8	11.4	21.0	32.5
Volts per turn	333	500	605	1,110	1,730
Space factor (in winding window)	0.158	0.121	0.110	0.087	0.054
Gross stacking height (inches)	80	65	58	80	100
Primary plus secondary I^2R-losses at rated load. Power factor = 1.0 and 170 degrees centigrade (kilowatts)	142	77	54	103	168
Core loss (kilowatts)	42	62	73	184	430
Allowance for stray load loss plus ventilation (kilowatts)	10	15	25	50	73
Total losses, power factor = 1.0 (kilowatts)	194	154	152	337	673
Rated load efficiency, power factor = 1.0 (per cent)	99.0	99.2	99.2	99.3	99.3
Weights					
Laminations	63,000	15,500	12,100	30,600	72,000
Conductors (aluminum)	1,700	900	600	1,200	1,900
All else (including tank, ventilating equipment, insulation, and so forth)	19,300	4,600	3,700	9,200	21,800
Total weight	84,000	21,000	16,400	41,000	95,700
Total weight per kilovolt-ampere	4.2	1.05	0.82	0.82	0.96
Per kilovolt-ampere weight of					
Laminations	3.2	0.78	0.61	0.61	0.72
Conductors	0.085	0.045	0.030	0.024	0.019
Laminations plus conductors	3.3	0.82	0.64	0.63	0.74
Laminations plus conductors in per cent of total weight (per cent)	79	78	78	77	77

These exploratory designs were carried only far enough to disclose trends clearly, to ascertain and check the general consequences of employing periodicities of 600 and 1,200 cycles per second. Attention is called to the fact that the conductor is of aluminum.

region of lower temperature or through an external heat exchanger.

The circulation speed of the gas will be high—500–1,000 feet per minute, controlled by a fan or blower.

As the core will operate around 300 degrees centigrade, and there will be a drop of about 100 degrees centigrade through the thermal barrier, the ascending gas will flow over barrier surfaces at a temperature of about 200 degrees centigrade. By virtue of the high rate of flow of the gas, no portion of it will acquire a temperature higher than 150 degrees centigrade.

The hottest parts of the windings may attain a temperature in excess of 200 degrees centigrade, the surface drop probably exceeding 75 degrees centigrade, and the average copper temperature by resistance being of the order of 170 degrees centigrade.

These hypothetical temperature values are chosen as well suited to the use of SO_2 at 50 atmospheres.

As a comparison between the heat-transfer capacity of compressed gas and air at atmospheric pressure, it may be pointed out that SO_2 at 50 atmospheres is about 71 times as efficient as atmospheric air for equal volume. This is based on a specific-gravity ratio of 110:1 and specific-heat ratio of 0.154:0.237, excluding considerations of viscosity and contact resistance. Thus, instead of the 130 cubic feet of air per minute per kilovolt-ampere for 55 degrees centigrade rise allowed in air-blast-transformer practice, only 1.8 cubic feet of compressed SO_2 at 50 atmospheres would be needed.

Supporting of Transformer Tanks

The forces exerted on the tanks by the compressed gas can be taken care of by the combination of:

(*a*). Providing a tank-wall thickness of $^5/_8$ inch.

(*b*). Providing internal braces in the form of tie rods of a nonmagnetic alloy.

(*c*). Locating the tank in an underground pit and filling in gravel and cement between the tank walls and the pit walls to provide support for the tank walls.

Comments on Sulphur Dioxide

If absolutely free from all trace of moisture, SO_2 gas is quite noncorrosive to some metals up to rather high temperatures. It costs only six cents per pound, whereas many otherwise attractive insulating gases are rather expensive. In a metal-sealed transformer tank complete absence of moisture may be maintained. Although more tests are desirable on the effect of temperature and pressure on the corrosive properties of this gas, rough tests indicate that a pressure of 40 atmospheres is entirely safe. At this pressure its dielectric strength is 2,400 volts per mil and the condensation temperature 120 degrees centigrade. At 50 atmospheres its dielectric strength is 2,500 volts per mil and condensation temperature 130 degrees centigrade. Doubtless reliable surface treatments (such as enameling) could be developed to render the metal surfaces immune from attack at much higher temperatures.

In this, as well as in all other gases, condensation of the gas on those portions of the system which need insulation must be prevented by keeping those parts hotter than the condensing temperature of the compressed gas.

Exploratory Designs

Five designs are exhibited in Table II in just enough detail to disclose clearly the *trends* and illustrate the *potentialities* of compressed-gas transformers. Three of the designs (*A*, *B*, and *C* for 80 kv) are for 20,000 kva each, one at 60 cycles, one at 600, and the third at 1,200. Design *D* is for 50,000 kva at 1,200 cycles per second, and *E* for 100,000 kva at the same frequency.

Design *A* (60 cycles per second) has a per kilovolt-ampere weight of 4.2 pounds, which is approximately half that of a conventional oil design, whereas designs *B* and *C* have weights of only 1.05 and 0.82 pounds per kilovolt-ampere, respectively. The full-load efficiencies vary from 99.0 per cent at 60 cycles per second to 99.2 per cent at 600–1,200 cycles per second, and can be made higher if desired. In order to save on critical materials aluminum, instead of copper, is figured on in all of these designs.

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Pentode Ignitrons for Electronic Power Converters

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Synopsis: The name "pentode ignitron" has been applied to a high-voltage tube used for rectification and inversion in electronic converters. It is a sealed steel tube having an anode, three grids, and a mercury-pool cathode. The tube is of the gas-filled type with control characteristics similar to those of the thyratron. An ignitor establishes a cathode spot each cycle on the mercury pool. The cathode spot is stabilized at low anode currents by the use of an auxiliary holding anode. The functions of the three grids are somewhat similar to those in the pentode vacuum tube, but the manner of use is quite different. The first grid, or one nearest the anode, is used as an intermediate anode to divide potential gradients across the tube during nonconducting periods. The second, or center grid, is used to control the starting of the conduction period and the third, or outer grid, as a shield to permit rapid deionization of the space between the control and shield grids.

The introduction of multiple grids in ignitron tubes provides certain characteristics which are essential to inverter operation. It is the purpose of this paper to describe some of these characteristics, and the design and construction of the pentode ignitron tube.

These tubes are the ones installed in the power converter described in the companion papers.[1,6–8]

THE history and development of suitable electronic tubes and circuits for high-voltage power-conversion systems is discussed in a companion paper.[1] Briefly, the first power unit after laboratory tests consisted of a 40–60-cycle 5,000-kw power converter. The tubes for this unit were of the thyratron* type. The nonsynchronous link operated at a direct potential of 30,000 volts and a current of approximately 170 amperes. Power from the 40-cycle system was converted to direct current through 12 phanotron tubes connected in a cascade arrangement of two three-phase double-way rectifier circuits. The direct current was converted to 60-cycle a-c power through 12 thyratrons connected in a similar arrangement of double-way inverter circuits. Initial operation was remarkably free from any basic trouble. Operation over a period of some five years demonstrated the complete practicability of sealed electronic tubes for this type of service.

The development of the ignitor[2] opened the possibility of utilizing the control features of the thyratron design in a tube having the inherent high-current capacity of the mercury pool. Operating experience with sealed steel ignitrons[3] which were developed for service in the 250- to 600-volt power-conversion fields indicated the basic soundness of the ignitron principle of starting a new cathode spot each conducting cycle. These ignitrons are of sufficient current capacity to give power rectifiers of 75 to 1,000 kw, depending on voltage. Similar ignitrons operate in welding service with precise cycle-by-cycle control of currents from 1,000 to 10,000 amperes. The rectifier ignitrons were designed to have the lowest possible arc loss consistent with essentially complete freedom from arc-backs under normal operation conditions. The designs consist of an anode, a mercury-pool cathode, ignitors, and a small auxiliary anode for maintaining the cathode spot during the normal conducting period in case the main anode current falls below the value (about three amperes) at which the arc is stable. There is a minimum of shielding. Voltage control is obtained by shifting the phase angle at which the ignitor is fired. Variation in the starting or ignition time is small (of the order of 50 to 100 microseconds) with the usual excitation circuits.[4] Deionization time at the end of conduction is relatively long because of the open construction. Deionization time in an ignitron is the time required for the ionization in the space to decay sufficiently so that cathode spots are not reestablished if the anode is made positive. In a thyratron, it is the time required for ion sheaths to close the holes in the grids and effectively isolate the anode from the main body of the tube. Ignitron tubes have operated successfully as inverters,[5] but the required commutation time is in general too long to permit optimum operation. For such service, the grid-control characteristics of the thyratron tube for accurate starting and rapid deionization, in combination with the high current capacity of the mercury pool, seemed to offer attractive possibilities for the design of higher-voltage higher-current tubes.

Description

The pentode ignitron tube consists of an anode, a mercury pool cathode with ignitors and auxiliary anodes, and three grids. The similarity of the construction—an anode, three grids, and a cathode—to that of the pentode vacuum tube, suggested the use of pentode ignitron as a name. The general design is shown in Figures 1 and 2. The anode is surrounded by the grids. Starting from the anode, the first grid acts as an intermediate anode to divide potential gradients during nonconducting periods. The next or control grid determines the time of starting and reduces the deionization period at the end of conduction. The outer or shield grid acts in a dual capacity to pick up the arc at the beginning of conduction and to shield the rest of the grid–anode structure from residual ionization after conduction. The cathode has three ignitors, only one of which need be used at a time. There are also two auxiliary holding anodes. One auxiliary anode maintains cathode-spot excitation for low anode currents and the other is used for control purposes as described later.

The tube conversion capacity is of the

Table I

Maximum peak inverse or forward voltage, volts	20,000
Maximum anode current, amperes:	
Instantaneous	900
Average:	
Continuous	150
Two hours	200
One minute	300
Minimum water flow, gallons per minute	3
Minimum inlet water temperature, C	35
Maximum inlet water temperature, C	45

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* Thyratron is a generic term describing a gas-discharge thermionic-cathode tube in which one or more grids control the starting of the current electrostatically.

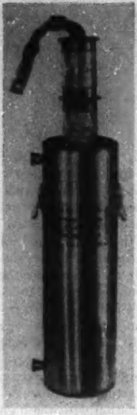

Figure 1. The pentode ignitron tube used in the 20,000-kw electronic power converter

order of 1,500 kw per tube. Companion papers[6-8] describe the installation and application of the pentode ignitrons in a 20,000-kw 25-to-60-cycle electronic power converter. The converter is divided into two 10,000-kw units. Each unit consists of two sections, a rectifier and an inverter, whose functions may be reversed, depending on the desired direction of power flow. There are 12 tubes in each section connected in two three-phase double-way rectifiers in cascade. The nonsynchronous d-c tie operates at approximately 30,000 volts and 340 amperes.

Development

The design of the tube is based on work by K. H. Kingdon and E. J. Lawton[9] on the cause of arc-back and on the possibility of increasing the operating voltage of gas-filled tubes through potential division.

A single-anode tube has a fairly definite maximum voltage limit at which arc-back occurs. This limit depends on load conditions, vapor pressure, and tube design. Reduction of potential gradients by means of potential dividing anodes would seem to offer the possibility of increasing the operating voltage. The principle has been successfully applied in high-vacuum rectifier tubes for X-ray service. In the case of gas-filled tubes, and particularly for tubes operating at relatively high currents, it is impracticable to divide the inverse potential at the end of commutation since the ion densities are too great. A few degrees later in phase angle, however, ionization has decreased to values which permit successful use of potential division.

Arc-back or inverse current conduction has been an interesting subject of discussion for many investigators.[10-12] The occurrence is definitely of a random nature, and the time may vary[13] from immediately after conduction when ion densities are greatest to any point in the inverse cycle. One obvious cause is too high vapor pressure due to lack of sufficient cooling or to mercury droplets striking hot objects and transiently increasing the vapor pressure above the voltage breakdown of the gas. The relation between vapor pressure and temperature for equilibrium conditions is shown in Figure 3. Mercury droplets are ejected from the surface of the pool by the action of the cathode spots. These droplets leave the pool with a random velocity so that the space above the pool and around the anode is filled with flying droplets during both the conducting and inverse parts of the cycle. These droplets do not always cause arc-back even when striking a hot anode, but it seems probable that a sufficient concentration of them may well do so.

As a result of tests on the influence of contaminating materials such as glass, quartz, asbestos, and so forth, on the rate of arc-back, the hypothesis has been proposed by Doctor Kingdon that arc-backs are due basically to the collection of charges on small insulating patches on the anode. If the potential gradient exceeds the field emission requirements of the base material, arc-back results. Whether or not a sufficient potential gradient is established depends on the rate at which ions strike the insulating particle and the rate at which the charge leaks off. The rate at which ions strike the anode is proportional to the ion density in the space in front of the anode at the end of conduction, and the negative voltage to which the anode is driven. The density of ionization bears a direct relationship to the current, and at the end of conduction both the current and inverse voltage may be defined in terms of circuit parameters. Test results indicate that the rate of arc-back is proportional to the product of the initial inverse voltage and the rate of current change. For each design of tube, therefore, a rate can be determined in terms of an arc-back factor δ which will

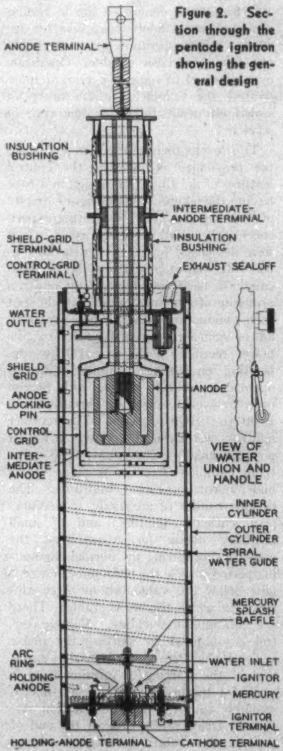
ANODE TERMINAL
INSULATION BUSHING
INTERMEDIATE-ANODE TERMINAL
SHIELD-GRID TERMINAL
INSULATION BUSHING
CONTROL-GRID TERMINAL
EXHAUST SEALOFF
WATER OUTLET
SHIELD GRID
ANODE LOCKING PIN
ANODE
CONTROL GRID
INTER-MEDIATE ANODE
VIEW OF WATER UNION AND HANDLE
INNER CYLINDER
OUTER CYLINDER
SPIRAL WATER GUIDE
MERCURY SPLASH BAFFLE
ARC RING
WATER INLET
HOLDING ANODE
IGNITOR
MERCURY
IGNITOR TERMINAL
HOLDING-ANODE TERMINAL
CATHODE TERMINAL

Figure 2. Section through the pentode ignitron showing the general design

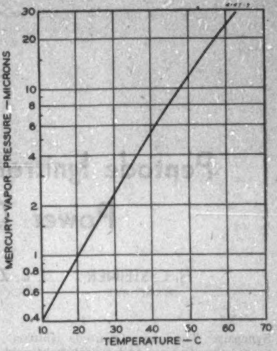

Figure 3. Relation between mercury-vapor pressure and temperature for equilibrium conditions

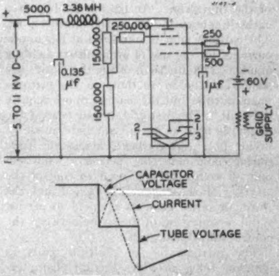
+
5000
3.38 MH
250,000
150,000
150,000
0.135 μf
250
500
1μf
60V
+
-
GRID SUPPLY
5 TO 11 KV D-C
-
2
1
2
1
3
CAPACITOR VOLTAGE
CURRENT
TUBE VOLTAGE

Figure 4. Single-tube inverter test circuit for investigating effect of circuit on arc-back rate

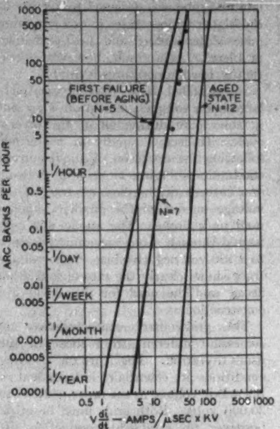

Figure 5. Relation between circuit arc-back factor $V(dI/dt)$ and rate of arc-back for the conditions shown in Figure 4

permit its performance under other circuit conditions to be predicted. That is:

$$\delta = V\frac{di}{dt}$$

where

V = negative anode potential at end of commutation in kilovolts

$\frac{di}{dt}$ = rate of current change at the end of commutation in amperes per microsecond

Developmental tests under varying circuit conditions of current and voltage were made to determine the arc-back rate. The tests were made in a single-tube inverter circuit as shown in Figure 4. The duty was made sufficiently high to cause rapid failure (one to ten times per minute), and the data obtained are shown in Figure 5. The solid lines are a plot of the Poisson probability formula:

$$P(n) = \frac{e^{-\mu}\mu^n}{n!}$$

where

$P(n)$ = probability of arc-back (arc-backs per number of trials in unit time)

μ = average number of ions striking an area ΔA in time Δt. This is proportional to $V\frac{di}{dt}$

n = number of ions striking ΔA in Δt time for failure

With the proper choice of μ and n the curve may be made to fit the test data, and the probable values of arc-back rates for more normal circuit constants determined from its extrapolation.

Arc-back tests were also made under actual power conditions in a biphase single-way rectifier–inverter circuit shown in Figure 6. The transformer capacity was of the order of 1,000 kw and the arc-back factor 1.5–2 for no phase retard. At 25,000 volts peak inverse, 150 amperes current, a limit of 60 degrees centigrade ($24^1/_2$ microns) outlet water temperature was found. At 12,500 volts, 300 amperes, the temperature was 71 degrees centigrade (50 microns). These extremely high limits tend to reflect the gas-breakdown point for a particular pressure and spacing.

Considerable experience and data are being accumulated to substantiate the validity of the insulating-patch theory and the definition of circuit duty in terms of an arc-back factor. Correlation between test conditions and actual performance in power circuits is sufficiently close to indicate the importance of the concept in understanding the mechanism of arc-back.

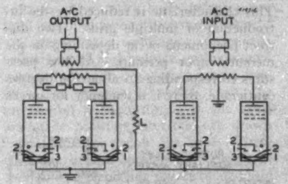

Figure 6. A 1,000-kw operation-test circuit for rectification and inversion

Control measurements were taken with a rotary switch in the circuit shown in Figure 7. In this case, a quarter-wave of current is passed through the tube under test. At the peak of the wave, the current is rapidly commutated to a second tube. Shortly after this, the mechanical switch opens and at a given interval later, a positive voltage is applied to the anode of the tube under test. Its ability to withstand this voltage is a measure of its deionization time, that is, its ability to regain control. Figure 8 shows the test results. The effect of gas pressure and grid-circuit impedance is clearly indicated.

Construction

The pentode ignitron is arranged for water cooling, that is, the mercury-vapor pressure is controlled by circulating water of the proper temperature through a water jacket surrounding the tube walls. A spiral water guide imparts a velocity of approximately five feet per second to the water to insure efficient heat transfer. An adequate amount of mercury-condensing surface is provided in the tube to maintain a low vapor pressure during overloads and at the same time allow for a reasonable variation of cooling-water temperature. Sudden increases in vapor pressure due to flying mercury droplets hitting hot anode and grid surfaces are prevented by the addition of a graphite splash baffle. The effects of ionization and vapor pressure are further controlled by suitable shielding in the anode stem and by the action of the grids.

Electrode connections are brought out through permanently vacuum-tight insulation bushings consisting of high-resistance borosilicate glass sealed to fernico.

All water-cooled surfaces of this tube are made of stainless steel which is highly resistant to corrosion. The inner cylinder is formed out of sheet material and a

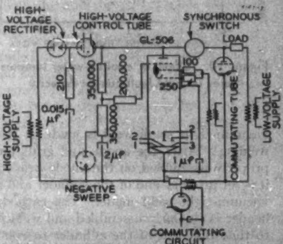

Figure 7. Kingdon–Lawton rotary-switch circuit for determining the recovery of grid control following commutation

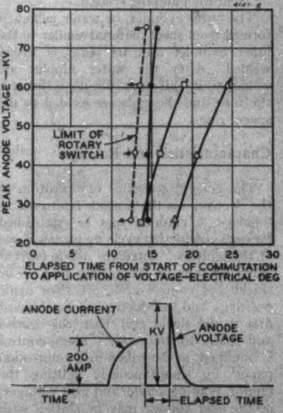

Figure 8. Grid-control recovery characteristics following commutation

▲ 60 C, control-grid resistance 500 ohms

□ 60 C, control-grid resistance 100 ohms

○ 40 C, control-grid resistance 100 or 250 ohms

● 40 C, control-grid resistance 500 ohms

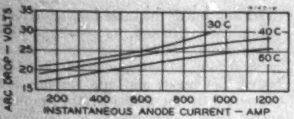

Figure 9. Arc-drop characteristics for cooling-water temperature of 30–60 degrees centigrade

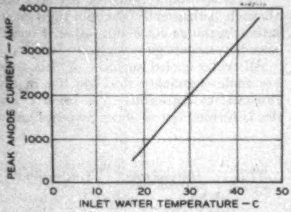

Figure 10. Effect of cooling-water temperatures on tube fault-current capacity

vacuum-tight seam weld is made by arc welding. After the anode and grids are properly assembled on the anode header, it is set into one end of the cylinder and a vacuum-tight weld made. The cathode header is similarly assembled and welded to the other end of the cylinder to complete the vacuum envelope of the tube.

At this point the tube receives a very rigorous degassing treatment during which the mercury is introduced and at the end of which it is permanently sealed off from the pumping system.

The outer cylinder, or water jacket, is formed from sheet material similar to the inner cylinder and its seam is fusion welded. After the water nipples are welded to it, the outer cylinder is slid over the tube and the ends are welded to the spacer rings.

Characteristics and Ratings

The general sequence of operation of the electrodes in the pentode ignitron is as follows. A cathode spot is established for each conducting cycle by means of one of the ignitors. Auxiliary anode current starts as soon as the ignitor fires. The shield and control grids are then made positive, and conduction is established first to the shield grid, then the control and finally to the anode. Phase control is obtained, as described in a companion paper[7] by simultaneously shifting the ignitor, holding anode, and grid voltages; or the grid voltage may be shifted with respect to the ignitor.

The introduction of multiple grids in the tube design considerably influences the tube characteristics. The arc drop is higher than that which is obtained in the lower voltage ignitrons. Figure 9 shows this characteristic. Some of the increase in arc drop is due to the length of arc path below the grids themselves. The arc drop plotted is the average value over the conducting period, that is, the arc-drop wave shape of gas-filled tubes is, in general, higher at the beginning of the period when there is a deficiency of ions and vapor and lower at the end of the conducting period. These values may vary from the mean by one to two volts. For the data in Figure 9, the tubes were operated in a three-phase single-way circuit with resistance load. The arc drop was measured by means of a cathode-ray oscilloscope and amplifier with an inverse-voltage blocking circuit. The abscissa is the maximum or peak of the current carried.

The amount of fault current which the tube can conduct is shown in Figure 10. This characteristic is reduced by the introduction of multiple grids. Two distinct phenomena occur depending on the mercury-vapor pressure. At low pressures corresponding to about 20 degrees centigrade, there is a tendency for surges if the current exceeds to any appreciable extent the value indicated. This phenomena is due to so-called "starvation" of the arc. In effect, all of the gas molecules in the grid openings are ionized and swept out of the immediate region before new molecules can diffuse in from the gas outside. The current tends to cease abruptly for an instant and this rate of current change, in connection with circuit inductances associated with the current path, generates high voltages. It is common practice in mercury-arc equipment to protect associated transformers with suitable resistance-capacitance filters or Thyrite.

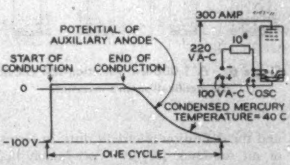

Figure 11. Potential of a probe near the cathode used to indicate ion density after conduction

At higher vapor pressures, corresponding to temperatures of 40 degrees centigrade and above, and higher currents, the arc drop tends to reach a value where cathode spots may form spontaneously on the walls of the tube. If this happens too frequently, sufficient metal may be vaporized into the mercury pool to cause wetting of the ignitors. Higher vapor pressures reduce the arc drop and increase the fault-current capacity, and it is desirable to operate the tube at as high a vapor pressure as is consistent with freedom from arc-back or loss of control. The pentode ignitron tube was designed to operate at the higher temperatures in order to minimize water-cooling and heat-exchanger problems. As a result, however, operation at low temperatures is limited.

In gas-filled tubes having relatively large open volumes, ionization is present in this space during all of the inverse cycle. It decays rapidly in magnitude following the cessation of anode-current conduction. Figure 11 shows a drawing of a cathode-ray oscilloscope trace of the voltage on one of the auxiliary anodes used as a probe. The anode was connected through a one-megohm resistance to a 100-volt negative bias. The voltage trace shows clearly the rate of ionization decay and the need for the shield-grid construction.

The grid-control characteristics are somewhat different from those of the usual thyratron. The "turn on" or firing conditions are essentially independent of the anode voltage in the range of 1,000 to 30,000 volts. Both grids must be made positive to establish conduction. If either is negative, the tube will not conduct. Conduction must first be established to the shield grid and then to the control grid. The optimum point for this occurs at the peak of the ignition-current impulse when an ionization density equivalent to the 40- to 50-ampere ignitor current is reached. Beyond this point, the ionization corresponds to that of the auxiliary-anode current and slightly higher shield-grid voltages are required. Under ordinary conditions, the shield-grid potential to establish conduction is of the order of 15 to 30 volts. Control-grid conduction voltages are correspondingly low. Figure 12 shows the grid-circuit control system used in the operation test, Figure 6.

The essential ratings defining the tube characteristics are tabulated in Table I.

Application

The application of electronic tubes involves careful co-ordination of the tube characteristics with the circuit requirements. The application engineer is concerned primarily with the over-all performance of the apparatus. The tube engineer must be concerned with the requirements as the tube sees them. While it is necessary to consider the average and rms voltages and currents, it is mandatory to consider the instantaneous.

Figures 13 and 14 show the anode-current and anode-to-cathode-voltage wave shapes for a tube in a three-phase double-way rectifier and in an inverter. The voltage traces are the logical result of rectifier and inverter operation in a three-phase system. In the case of the rectifier, the anode voltage is driven sharply negative at the instant the current transfer is complete. The resultant arc-back factor $V(di/dt)$ and the stress on the tube will be high when the voltage is high. It is increased with greater angles of retard since both the initial inverse voltage and the rate of current change increase. The remaining notches in the inverse voltage are the result of commutations between other tubes. In the case of the inverter, the small negative-voltage region represents the time for deionization and regaining grid control, as well as margin for possible phase-angle changes between the anode and grid circuits. Loss of commutation in an inverter tube results in a d-c short circuit on the rectifier 60 degrees later when the second tube on the same phase starts to conduct. Sudden voltage changes as represented by the vertical parts of the curve transiently may effect other elements in the circuit or tube. Arc-backs during the commutation period, interestingly enough, do not always result in loss of control. Currents are small and commutate out at the voltage reversal, and the arc does not restrike unless the ionization is high.

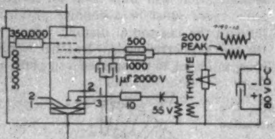

Figure 12. Grid circuit used in the operation set, Figure 6

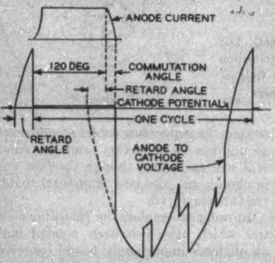

Figure 13. Diagram showing the instantaneous anode current and anode-to-cathode voltage for a tube in a three-phase double-way rectifier circuit

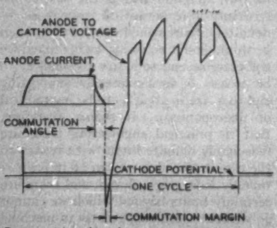

Figure 14. Anode current and anode-to-cathode voltage for operation in a three-phase double-way inverter circuit

Ample power in the ignition circuits is essential to insure complete reliability of firing for possible voltage fluctuations or changes in the ignitor. A single-cycle misfire in a rectifier results only in a partial loss of output and a corresponding increase in duty on the tube which must carry current for a longer period. A single-cycle misfire in an inverter, however, results in a short circuit on the rectifier since the direct current may then flow through the two tubes on one phase rather than traverse the transformer windings. In such a case, recovery is obtained by blocking the rectifier through ignitor and grid control to stop the direct current, and then gradually reapplying voltage. The entire cycle requires less than one-third second to restore power.[1,7]

Under certain conditions of residual ionization and high voltage, cathode spots may be formed in event of an ignitor misfire. The process is erratic, but the mechanism is interesting. If the grids are made positive, electrons are drawn from the space below the grid. These electrons attain considerable energy if there is no replenishing source to maintain the plasma electron and ion balance. In striking the grids they release secondary electrons. Ionization follows and the gradients established are sufficient to form cathode spots on projecting points in the cathode section or on the walls of the tube. The process is entirely prevented if the grids are held negative. Therefore, to prevent the possible uncontrolled formation of cathode spots the grid circuit is energized through one of the auxiliary anodes, as described in a companion paper.[7] The possibility of occurrence is remote, but its control illustrates the exactness which can be obtained when there is complete co-ordination between the tube and circuit characteristics.

Ignition, in general, with the excitation circuits employed, is both accurate and reliable.

The successful application of an electronic tube depends on its performance and life. In performance, it must equal or exceed that of other devices supplying the same function or it must provide some unique function in order to merit consideration. Life then becomes largely a matter of economics. In most power-conversion applications it is a balance between the savings due to the increased efficiency of the electronic apparatus and the cost of tube renewals. Industrial-tube life is excellent and in general results in a satisfactory operating cost. Since there is no way of running accelerated life tests, a conclusive statement cannot be made in the case of the pentode ignitron, but in view of experience with similar tubes and the basic design features involved, it is expected that an average life of at least five years will be obtained.

Conclusion

The development and application of the pentode ignitron marks a new and advanced step in the program of developing electronic tubes for power-conversion purposes. The performance to date and the characteristics of the 20,000-kw electronic power converter are described in associated papers. Quite obviously, additional units could be added to give higher voltages, and the d-c link itself could be extended to form a complete transmission system. As experience is gained, the tubes themselves will undoubtedly undergo design refinements and improvements.

The development and application of any electronic tube is interesting—that of the pentode ignitron extremely so. Its design and manufacture required the collaboration and the skill of many individuals, and their co-operation and assistance is gratefully acknowledged. In any extended development of this nature, vision and faith are required on the part of management and the authors wish to acknowledge specifically the encouragement and assistance of E. F. W. Alexanderson, R. C. Muir, and the late C. W. Stone.

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Dimensional Studies of Lightweight Motors for Aircraft

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Synopsis: The study of what happens to the proportions of a machine as the size of the machine is changed not only is interesting to imaginative engineers but also is very useful in teaching the engineer how best to design a particular machine. By understanding the relationship which exists between size and any desired useful characteristic, it is possible to make better use of experience which has been obtained on machines of different output in determining the probably best proportions of a new device. It is the purpose of the present paper to point out how relatively complicated machines may be compared by a method similar to dimensional analysis for the purpose of establishing such relationships as output per pound or output per cubic inch. Obviously, the conclusions which will be reached by such a study will be different as different original assumptions are established and it is therefore important to determine carefully what limitations on machine performance are really fundamental. Once such assumptions have been established soundly, it seems possible to arrive at very useful dimensional-analysis conclusions in connection with relatively complicated machines.

The Effect of Size on Design Proportions

It is not recorded by Jonathan Swift in describing the visit of Lemuel Gulliver to the island of Lilliput whether Gulliver observed the difference in engineering proportions which must of necessity have taken place between the machines of people of our size and those of the diminutive people who inhabited that island. If he had reported such differences they would have been the inspiration for some very interesting engineering philosophy concerning changing proportions as the dimensions of devices are changed in a way similar to that of microphotographing V-mail letters. A brief consideration of what happens when size is changed will show that, for equal performance, the best proportions of a machine are seldom the same. This idea is visualized easily if consideration be given to a simple mechanical structure and the conditions surrounding the change of its proportions as one of its dimensions is changed.

One of the simplest mechanical structures is that of a bar of structural steel suspended from one end and acted upon by the force of gravity. In considering the characteristics of such a piece of mechanism as the length of the bar is increased, the conclusion shortly is reached that it would be impossible to increase the length of the bar indefinitely if it be presumed that the cross section of the bar remain constant. When the structural steel bar had reached a length of approximately 33,000 feet, or six miles, a further increase in the length of the bar would cause it to break at the point of support under its own weight and it would, of course, be incapable of sustaining any external load. If, instead of being made of structural steel, the bar had been made of the rarer tungsten, the per unit strength of the material would, of course, be increased approximately four times, but unfortunately the weight of the metal also would be increased approximately two and one-half times and the length of the rod which would be self-supporting would be increased only 60 per cent and the limit in length, while greater, would be just as definite as for the steel rod.

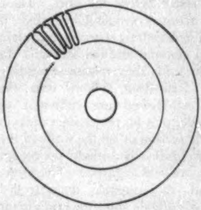

Figure 1 (left). D-c rotor lamination with teeth mechanically weak

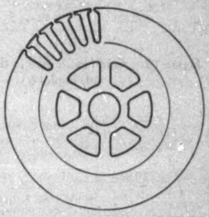

Figure 2 (right). D-c rotor lamination made with lightening holes

We are forced to conclude, therefore, that in the case of this particular structure one of the dimensions, the length of the bar, could not be increased indefinitely although the sectional dimensions of the bar could. It follows, therefore, that, if we had a miniature steel bar four inches long and 0.0001 inch in diameter suspended from one end and we wished to increase all dimensions of the bar proportionally, we could do so until each dimension was multiplied by 100,000 after which the bar would break of its own weight. Furthermore, as all the dimensions of the bar were increased, the useful load which could be attached to the extreme end would decrease from an initial value of 100,000 times its own weight to zero when each dimension was increased by 100,000 times.

An interesting sidelight of the above comparison is that it has been found that drawn wires of small cross section have a greater per unit strength than do drawn bars of larger section. For this reason, if the cross section of the rod or wire which we have been considering were made exceedingly small, for example 0.005 inch in diameter, the strength of the steel itself might be doubled or even trebled without any change in the chemical composition of the material. We see therefore that, instead of dealing with a constant for the ultimate per unit strength of a material, we actually have to deal with a variable which is dependent on size.

A slightly more complicated structure is that of a uniform flexible rod suspended from both ends in such way that the rod takes on the shape of a catenary. A brief consideration will show that for a catenary there must be a definite physical limit in the horizontal distance between the two supported ends. In other words there is a limit to the span of a suspension bridge, the supporting cables of which are of uniform cross section, and this limit still will exist even though the best sag is chosen and the best structural materials are employed.

In order to combat the limitations on size which have just been pointed out, an engineer immediately would conceive the possibility of employing tapered sections and by such an engineering trick he would be able greatly to extend the dimensional limits to which his structure could be produced without failing under its own weight. But if the size of his structure were permitted to increase indefinitely, there would come a time when he would find it necessary either to reduce the extreme end to a size so small as to be useless, or to increase the supporting end to a size so great as to be impractical or uneconomical. It seems, therefore, that in practical engineering there are reasonably definite limits with regard to the extent to which we can go in proportionally enlarging a device and there are certainly limits beyond which we cannot go without sacrificing greatly in machine efficiency.

Engineers, because they see so many things to do and so many things to be

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improved, are inclined to be busy people. Because of their tendency to be busy they seldom take enough time to philosophize on their problems and thus to recognize and obtain the best possible use of their experience. Ordinarily they are concerned more with producing one particular design to meet the requirements of the problem in hand, and once satisfactory performance has been attained, they tend to lose further interest and proceed with the next problem. Where time is not too important and competition not too severe, the tendency of cumulative experience is to produce finally a product of substantially the best proportions. However, when there is immediate pressure for an ultimate design of considerably altered size, the usual specific approach is likely to be either ineffective or expensive.

Analysis of Proportions of Lightweight Motors for Aircraft Use

The recent requirement brought about by the pressure of war necessity for many different d-c motors of minimum weight per horsepower for operating various devices on war planes is one in which the dimensional-analysis point of view can be brought to bear profitably. In determining the best proportions for such motors it is necessary to consider the best operating speed, the best number of poles, and the best relation between diameter and length in order that maximum output per pound may be secured. Because of the current interest in this problem insofar as war aircraft are concerned, it is one of the purposes of this paper to present a method for comparing d-c motors of different sizes. Another object of the paper is to describe a few of the conclusions which have been reached as a result of the study of such motors.

Basis for D-C Aircraft-Motor Study

It seems in consideration of the design of d-c motors for aircraft that the most important problems are specifically the following:

1. What are the best proportions for a d-c motor when weight is a prime consideration?
2. What are the relative horsepower per pound values which can be obtained from two, four, six, eight, and more poles?
3. How does the horsepower per pound vary with the stacking length?
4. How does the horsepower per pound vary with the size of the motor?

The structure of a d-c motor is so complicated as to make it entirely impractical to undertake to develop a function which would express the output of such a motor in terms of several independent variables, and it is necessary to apply practical considerations such as are used in design work in order to reduce the problem to one which can be treated analytically. Such considerations will be explained briefly.

It is obvious that the more complicated the structure of a motor (the more slots we put in the rotor, for example) the less efficient will be the use of the space and material which we allot to its construction. If too many slots are put in the rotor (Figure 1), the teeth may become flimsy, and it may be necessary to increase their widths for purely mechanical reasons. In addition, since the walls of each slot must be insulated in preparation for the winding, with a larger number of slots the amount of space which must be taken for slot insulation will be greater than for a small number of slots. For this reason it is desirable to maintain the number of rotor slots at a minimum in order to be able to crowd into a small space the maximum amount of active material. In direct conflict with the desire for a small number of rotor slots is the desire for good commutation, and in order to obtain good commutation a relatively large number of rotor slots must be used. It is necessary to use experience and designer's judgment to determine the number of rotor slots per pole which constitutes the best compromise.

The type of mechanical construction which is chosen for both the rotor and the stator will affect the utilization of material and the over-all size of the motor. Consideration must be given to whether unused iron under the rotor slots will be punched out (Figure 2) in order to lighten the rotor, or whether a riveted type, a shell type, or some other form of stator construction will be used. Once these constructional details have been fixed upon, it is possible to proceed with the analysis by making additional simplifying assumptions.

In order to avoid unnecessary complication it is well to confine attention to the electric part of the motor and to allow the end bells, the ventilation, and the bearings to intrude only insofar as they affect the electric construction. Even the commutator and brush mechanism may be relegated to the sidelines with the assumption that for any given horsepower of output the brushes must be of a certain cross section area and the commutator must be of sufficient size to receive such brushes. On the basis of this point of view there will be little difference in commutator size and weight for a given machine output with changes in electric proportions even with changes in numbers of poles.

If the motor is of the shunt type or is compounded only slightly, it can be assumed that the flux densities in the motor will be reasonably constant except for armature reaction and that the iron is worked very close to the saturation point. Under these conditions the performance of the motor is very similar to that of a separately excited machine, and the variation in output characteristics may be assumed to come almost entirely from the variation in rotor proportions.

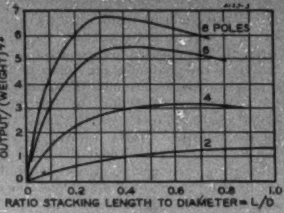

Figure 3. Calculated curves, output characteristics versus poles and L/D

If we are dealing with a constant field flux, the output of a d-c motor is a function of the amount of conductivity in the rotor winding and is independent of the actual number of turns on the winding provided the impressed voltage is assumed to be adjustable to obtain best motor performance. In other words, it is unnecessary for us to concern ourselves with the actual details of the rotor winding itself provided we know the resistance of the winding corresponding to a given number of rotor-winding turns. This point of view allows us to take the mean length of the rotor turn divided by the total area of the rotor conductors as a measure of the output of the rotor.

If by design modification it becomes possible to increase the field flux of a d-c motor, the effect is to cause the motor to run more slowly unless there is a proportional increase in the voltage impressed upon the rotor. Since the speed is usually a part of the specifications, it would be normal either to modify the rotor winding or to increase the impressed voltage by such an amount as to maintain the speed of the motor at the same value as before the increase in the field flux. If the voltage per turn of the rotor be modified to maintain constant speed, the actual rotor resistance per impressed volt will vary inversely as the square of the field flux, and, consequently, the output of the rotor also will vary as the square of the field flux.

From the above considerations the complete expression for the output of a constant-flux d-c motor operating at constant speed is

$$\text{Output} = (\text{field flux})^2 \times (\text{rotor conductivity}) \tag{1}$$

From the above expression it is possible to start with a rotor diameter and stacking length chosen to give approximately the output desired and to lay out within that diameter a series of rotor punching designs differing from one another by the width of the rotor teeth and other changes which follow consistently therefrom. On the assumption that the flux density will be maintained constant in the rotor teeth by varying the field strength, it is possible to express the rotor output as the product of the square of the total rotor-tooth section by the total useful rotor-slot area divided by the rotor mean length of turn. In this way the relative merits of a series of rotor designs can be determined without the labor of calculating each specifically and in detail. By plotting a curve in which the above product is the ordinate and the width of the rotor tooth the abscissa, it is possible to select the particular rotor design which will give the maximum possible output within the assumed dimensions. With a change in the assumed stacking length it would be expected that the rotor punching design would change somewhat in order to secure the best proportions, although actual trials, fortunately, show that the effect of stacking length on the best punching design is not very pronounced. Once the best proportions for a rotor having a given ratio of diameter to stacking length have been determined it can be shown that a proportional change in all dimensions of the rotor will not affect the condition of maxima. In other words, if a certain rotor design having an outside diameter of D is found to be the best design for a given stacking length, L, then a rotor having every dimension reduced to one half the size of the first mentioned rotor will be found to have the best proportions for a rotor of that diameter and length. With this in mind it is possible to apply dimensional analysis to the rotor design and thus compare rotors having different sizes but which are otherwise similar.

The dimensional equation corresponding to the field flux of a rotor with saturated teeth is

$$\text{Field flux} = L^2 \tag{2}$$

The dimensional equation corresponding to the conductivity of a rotor winding as already defined is

$$\text{Conductivity} = L^2/L = L \tag{3}$$

By substituting the dimensional values for flux and conductivity in equation 1, the dimensional equation for output is

$$\text{Output} = (L^2)^2 \times L = L^5 \tag{4}$$

The dimensional equation for weight is:

$$\text{Weight} = L^3 \tag{5}$$

$$L = (\text{weight})^{1/3} \tag{6}$$

When equation 6 is substituted for L in equation 4, we learn the output in terms of the weight, which is

$$\text{Output} = (\text{weight})^{5/3} \tag{7}$$

Equation 7 shows that the output of a motor under the conditions assumed is not linear with the weight as often is assumed, but instead varies as the $^5/_3$ power of the weight. In other words the larger a motor becomes, the greater will be its output per pound. In order to compare two different designs it is necessary to reduce them to two designs of equal weight. This may be done by dividing the output of a design by the $^5/_3$ power of its weight, which is equivalent to reducing the design to a proportional one having a weight of one pound. On this basis, the dimensionless number which measures the output per pound of any design under the conditions assumed is

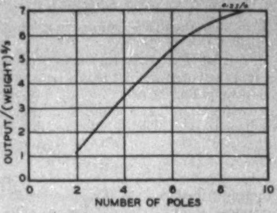

Figure 4. Calculated curve, maximum output characteristic versus number of poles

$$\text{Design effectiveness} = \text{output}/(\text{weight})^{5/3} \tag{8}$$

By making a series of designs, as previously suggested, to determine the best design for a given diameter and length of rotor and then repeating the operation for other ratios of L/D, it is possible to plot design effectiveness figures and to learn what ratio of L/D gives the maximum output per pound for a motor of any given number of poles. A similar investigation for other numbers of poles permits plotting a curve showing the maximum output per pound as a function of the number of poles of the motor.

It is not the purpose of the present paper to go into the laborious side of the problem, but only to indicate in a general way the method to be followed in making an analysis of this sort and to show the types of curves which are obtained in such an investigation. These curves are given in Figures 3 and 4.

Effect of Speed on Output

In many cases because of the use of a gear train, the speed at which an aircraft motor operates will be left to the judgment of the designer of the motor and the gear train. Under such conditions it is desirable to be able to compare the over-all weight of a high-speed motor driving a long gear train with a low-speed motor equipped with a shorter train. The foregoing discussion furnishes the means for making such a comparison.

The speed of a constant-flux d-c motor can be increased by increasing the volts per turn which are impressed on the rotor winding. If the volts per turn are increased in ratio a, the increase may be accomplished by reducing the number of rotor turns in ratio $1/a$. Since a reduction in turns produces a corresponding reduction in length of conductor and, in addition, a corresponding permissible increase in the area of the conductor, the actual resistance of the rotor will be reduced in ratio $1/a^2$. If the duty cycle is so short that heating is not a limit in the motor performance, the change in speed will be accompanied by an increase in output which is proportional to the square of the change in the volts per turn. In other words, where heating is not a limit

$$\text{Output} = \text{speed}^2 \tag{9}$$

Of course, it will be noticed that an increase in the speed of a motor should change the ratio of the weight of the commutator and brushes to the weight of the active material of the motor and therefore should modify by that amount any conclusions which are reached in connection with the active material of the motor.

If the temperature of the rotor be a limit, it is necessary to maintain the same current density in the rotor regardless of the speed. In such case, the output of the motor would be directly proportional to the speed, and for a given motor size and constant rotor current density, we might write

$$\text{Output} = \text{speed} \tag{10}$$

It depends upon whether the duty cycle of the motor is short or long which of the foregoing two relationships will be assumed to exist and, if temperature is not a limit, a more complete expression to be used in comparing the output of two motors than that given in equation 8 might be written as follows:

$$\text{Design effectiveness} = [\text{output} \div (\text{weight})^{5/3}](\text{speed})^2 \tag{11}$$

Conclusions

It is especially interesting to notice that under the assumed conditions there is a considerable advantage in output per pound in increasing the number of poles of a motor until eight or ten poles have been reached. Beyond this point there is relatively less improvement to be gained by increasing the poles, and manufacturing considerations and considerations of a practical nature dictate that no more poles than are necessary to accomplish the desired result should be used.

In considering the value of the curves which have been obtained by the admittedly high-handed method of analysis which has been proposed herein, there may arise questions concerning the validity of the conclusions in connection with motors other than the constant-field shunt type of motor which has been discussed. It probably might be better to start with different and perhaps more complicated original assumptions in the

Design and Operating Characteristics of Modern Dry-Type Air-Cooled Transformers

W. W. SATTERLEE
MEMBER AIEE

A DRY-TYPE transformer may be defined as a transformer devoid of all liquids and one whose core and coils and associated parts are cooled by the natural or forced circulation of air.

Dry-type air-cooled transformers are not new; in fact, the first transformer ever built was of dry-type design. However, until a relatively recent date, dry-type transformers have been usually of small kilovolt-ampere rating and low voltage, or of relatively large size of the air-blast type. These transformers have been constructed with organic insulating substances, known as class *A* insulation. Class *A* insulation is not fireproof.

In 1936, a new kind of dry-type transformer was developed. These transformers are now built in ratings as large as 4,000 kva and they are cooled simply by the natural circulation of air through their core and coil structures. With forced-air cooling the capacity of these transformers may be increased materially.

These dry-type transformers are explosion-proof and essentially fireproof because their major insulation is principally air and porcelain. In fact, their insulation structures are made up largely of inorganic substances, such as porcelain, glass, asbestos, and mica, or combinations of these and other substances, bonded together with a heat-resisting varnish. Combinations of these substances are now defined in American Standard *C*-57.1 as class *B* insulation.

Temperature Limitations

Briefly stated, the present definition of class *B* insulation covers inorganic substances such as mica, asbestos, and fiber glass in built-up form with organic binding substances. A small portion of class *A* materials may be used for structural purposes provided the electrical and mechanical properties of the insulating structure are not impaired by a continuous temperature of 130 degrees centigrade.

Although the continuous temperature limit specified for class *B* insulation was established some time ago at 130 degrees centigrade it is obvious from the definition of class *B* insulation that there might be different combinations of organic and inorganic substances that would have temperature limits varying over a considerable temperature range. This is further indicated in AIEE Standards number 1 by the statement: "Materially higher 'hottest-spot' temperatures may be satisfactory for particular forms of class *B* insulation, . . ." Consequently, it has been recognized that certain combinations of these substances will withstand higher operating temperatures than 130 degrees centigrade. Such a combination of insulating materials has been used in developing the dry-type transformer described in this paper.

Now, let us consider just how these insulating materials are used in the construction of these transformers so that they will give satisfactory service over long periods of time even though under sustained rated load their "hottest-spot" copper temperatures may be in excess of 130 degrees centigrade.

Figure 1 is a photograph showing the structural detail of the type of winding commonly used in these transformers for operating voltages up to 5,000. Figure 2 shows the type of winding used for voltages above 5,000 and up to and including 15,000.

Winding conductors are insulated with glass or asbestos. Sections of the high- and low-voltage windings are separated by porcelain, mica, or asbestos. High-voltage windings are supported on porcelain. Porcelain is used to center the high- and low-voltage winding assembly on the core. Porcelain spacers form large

Table I. Insulating-Barrier Hottest-Spot Temperature Rise Expressed as Per Cent of the Hottest-Spot Copper Temperature Rise

Duct Length, Inches	Duct Width		
	1/2 Inch	3/4 Inch	1 1/2 Inches
15	58	52	52
30	67	58	52
45	73	62	52

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consideration of the series motor and perhaps even in the consideration of a shunt type of motor in which the armature reaction very seriously affects the field distribution. However it is true that for motors of the aircraft type it probably will be found that under the normal operating conditions the strength of the series field is so great as substantially to saturate the magnetic structure of the motor and to produce conditions more nearly like those proposed in this discussion than might appear at first glance. It is probably also true that there is enough similarity between a series motor and a shunt motor to make the end result of an individual study of each reach approximately the same conclusion.

The procedure outlined in the paper has not included the study of the field structure of the machine, although, actually, work which already has been carried out did include the field structure in order to complete the story and obtain an accurate picture of the output per pound of active material in the motor.

It is believed that the conclusions which have been reached in connection with the study should make it possible to compare motors of this type which differ greatly from one another in size more accurately than by the more conventional D^2L method of comparing designs.

General Comments

As mentioned originally in connection with the comments on proportions of mechanical structures, there are factors which dictate that as the size of an electrical machine is increased indefinitely its proportions must change. For this reason, a study of machine effectiveness made over a considerable range in size must be accompanied by a review at intervals of the design limitations to make certain that a factor which was important in a small machine does not become negligible in a large machine or vice versa.

One important factor which was omitted purposely from the present study was the effect of iron loss. Such loss will be seen to vary in respect to the output as the output is increased while the flux density is maintained constant; because with flux density constant the iron loss will vary as the first power of the weight, whereas the output has been shown to vary as the $^4/_3$ power of the weight. With two similar machines differing in size the iron loss of the large machine should be less important in comparison with the output.

air-ducts between sections of the high-voltage winding and between the low-voltage winding and the core. A clear cylindrical duct of air separates the low-voltage winding from the high-voltage winding. Dirt which falls into this high-potential area simply drops through to the bottom of the transformer case. Horizontal creepage surfaces in high-potential areas are eliminated. High-voltage and low-voltage leads are supported on or terminated in porcelain insulators.

Figure 3 shows a cross section of a low-voltage and high-voltage winding and the insulation structure. The windings are individually treated with a heat-resisting thermoset varnish and baked at a high temperature. The core and winding assemblies are also treated with the same heat-resisting varnish and baked at a high temperature which eliminates the residual volatile matter. This treatment imparts a smooth, tough, glossy finish to the complete structure which makes it highly resistant to heat, dirt, and moisture.

Glass, an inorganic substance, is highly resistant to temperature. It is also essentially nonhygroscopic. However, in order to insulate copper suitably with glass for use as a winding conductor, it is necessary to bond the glass fibers and filaments mechanically to the copper. This bond imparts the mechanical stability required to use the insulated conductor in winding coils and assembling them into transformers. Tests made on windings treated with this bond show that it will withstand continuous temperatures of the order of 160 degrees centigrade for long periods of time with but little loss of its mechanical cementing properties. The insulation is not materially affected by an amount of moisture that may be expected under operating conditions. Operating experience has confirmed that it is not necessary for the varnish to retain its original properties fully during the operating life of the transformers, as will be shown later.

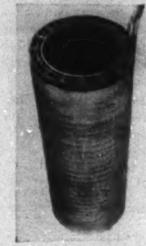


Figure 1. Structural detail of a barrel-type low-voltage winding

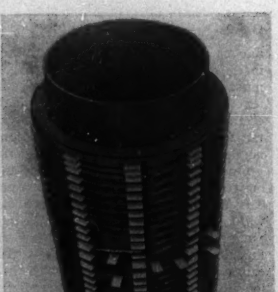


Figure 2. Structural detail of a disk-type high-voltage winding

Temperature Distribution

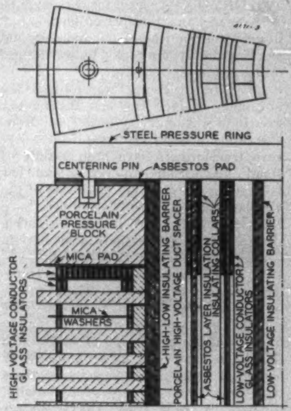
Let us now consider what the operating temperatures of the windings and the insulation structures may be and how the transformers are assembled to withstand these temperatures.

According to American Standard *C*-57.1, dry-type transformers constructed with class *B* insulation (combinations of organic and inorganic substances) are permitted to operate at a continuous average copper temperature rise of 80 degrees centigrade, measured by the resistance method. With the present maximum permissible temperature of 130 degrees centigrade for class *B* insulation and with an ambient temperature of 40 degrees centigrade the hottest-spot copper allowance in a transformer would be only 10 degrees centigrade. This seems inconsistent because even in properly designed dry-type transformers of small kilovolt-ampere rating, aside from those of large kilovolt-ampere rating, relatively high temperature gradients may exist and it is not practicable to limit the hottest-spot allowance to 10 degrees centigrade.

If we consider the actual temperatures to which the insulation of a dry-type transformer may be subjected, it is necessary to recognize the existence of temperature gradients within the transformer itself. The insulation nearest the winding copper is necessarily subjected to higher temperatures than the insulation physically removed from the copper. Thus the turn insulation, directly in thermal contact with the copper, is, for all practical purposes, subjected to the same temperatures as the copper. On the other hand, insulating barriers which are not in thermal contact with the copper will be at appreciably lower temperatures than the windings.

To obtain data on temperature gradients within dry-type transformers, tests have been made on model winding assemblies, representative of small as well as large transformers, and on actual transformers, to determine (a) copper temperatures and their distribution in the windings of transformers as representative of the insulation in direct contact with the copper, and (b) temperatures of the insulating barriers in transformers as related to the copper temperatures and the space between the copper and the barriers.

Figure 4 shows a model winding assembly on which a series of tests were made by passing a certain current through the winding until steady temperature conditions were obtained and recorded by thermocouple readings. Thermocouples were used because they afford the most suitable experimental means of measuring local temperatures.



STEEL PRESSURE RING
CENTERING PIN
ASBESTOS PAD
PORCELAIN PRESSURE BLOCK
MICA PAD
MICA WASHERS
HIGH-VOLTAGE CONDUCTOR GLASS INSULATORS
HIGH-LOW INSULATING BARRIER
PORCELAIN HIGH-VOLTAGE DUCT SPACER
ASBESTOS LAYER INSULATION
INSULATING COLLARS
LOW-VOLTAGE CONDUCTOR GLASS INSULATORS
LOW-VOLTAGE INSULATING BARRIER

Figure 3. Cross section of the low-voltage and high-voltage winding and the insulation structure

The winding assembly consisted of three sections, each section 15 inches long. The three sections could be used either separately or stacked one upon the other, thus giving three effective winding lengths. The width of the cooling duct separating the winding from the insulating barrier was made variable by using barriers of different diameters. Thermocouple junctions were soldered to the copper conductors. A three-quarter-inch retaining collar at the ends of each section reduced the effective length of the winding assembly to $13^1/_2$ inches per section.

In all these tests, it was found that the hottest-spot copper temperature is located at a distance of 10 to 15 per cent below the top of the winding, the higher value applying to the wider ducts. For any specific duct width and length, the hottest-spot copper temperature rise was found to be proportional to the average copper temperature rise, thus giving a constant value for the ratio of the hottest-spot copper temperature rise to the average copper temperature rise. This ratio was found to vary with the width and length of the air duct separating the winding from the insulating barrier. This is shown in Figure 5. The highest value was 1.36 for the longest and narrowest duct.

Due to the presence of a radial gradient and a reduced radiation at the ends of the winding because of the retaining collars, the ratio of the hottest-spot copper temperature rise to the average copper temperature rise may be expected to be greater in the windings of actual transformers when compared with that obtained in the model winding.

To determine the location of the hottest-spot temperature in the windings of actual transformers, a series of tests was made on a 300-kva 2,400-volt single-phase transformer, and on several 500- to 800-kva 13,800-volt three-phase transformers. All of these transformers had a pancake or disk-type outer high-voltage winding with three-eighths-inch air ducts separating the disk sections. A three-eighths-inch air duct separated the inside of the winding from the outside of its insulating barrier. The inner or low-voltage windings were of the cylindrical or barrel type of two- and four-layer construction having air ducts from five-eighths inch to seven-eighths inch wide, separating the layers from each other, and from the inner and outer insulating barriers.

The 300-kva transformer had a number of thermocouples placed in the low-voltage and high-voltage windings, the object being, primarily, to locate the hottest spot. Tests made on this transformer show that for the same average temperature in each winding, the upper portion of the inner or low-voltage winding is considerably hotter than the hottest part of the outer or high-voltage winding, thus definitely locating the hottest-spot of the assembly in the upper part of the inner winding. A study of the radial temperature distribution in the inner winding shows the existence of a temperature gradient from layer to layer with the highest temperature in the outer layer.

The highest value of the ratio of the hottest-spot copper temperature rise to the average copper temperature rise recorded for natural draft operation on the 500- and 800-kva transformers, was 1.41. With forced-air operation of these transformers and with the same average copper temperature rise, the value of this ratio, in general, may tend to be somewhat higher. In particular, this may be due to the more turbulent action of the air which exists with high air velocities. In an extreme case, however, a high value of the ratio of the hottest-spot copper temperature rise to the average copper temperature rise may be traced to an unequal distribution of the air flow between the phases of the transformer and a consequent inequality of the average copper temperature in the different phases.

A comparison of actual copper and barrier temperatures obtained is as follows: Based on an average temperature rise of the windings of 80 degrees centigrade as determined by the resistance method, the hottest-spot copper temperature rise will range from approximately 100 to 115 degrees centigrade. With an ambient temperature of 40 degrees centigrade the hottest-spot temperature of the copper will range from approximately 140 to 155 degrees centigrade.

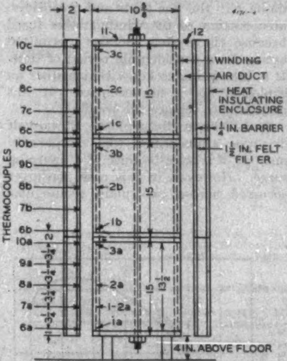


Figure 4. Model winding and barrier assembly used for temperature distribution tests

Data obtained on the model winding assembly, Figure 4, show temperatures on the surface of the insulating barrier adjacent to the winding but separated from it by air ducts of variable widths. Figure 6 shows a typical temperature distribution in the winding and the barrier with a one-half-inch-wide air duct separating the winding and the barrier. Expressed as a fraction of the hottest-spot copper temperature rise, the value of the barrier hottest-spot temperature rise varies from 0.58 to 0.73.

It has been shown that the barrier insulation in these transformers will operate at appreciably lower temperatures than those of the windings. See Table I and Figure 6. From these data, it may be noted that the hottest-spot temperature of the barrier insulation in properly designed dry-type transformers having an 80-degree centigrade average winding temperature rise as measured by the resistance method will be approximately 115 degrees centigrade; that is, it will range from 25 to 40 degrees centigrade below the hottest-spot winding temperature. Life tests on the barrier insulation used in these transformers show that it will withstand a continuous temperature of 130 degrees centigrade without detrimentally affecting its electrical and mechanical strength.

It is important to note that none of the insulation which may reach a temperature in excess of 130 degrees centigrade is subjected to line-to-line or line-to-ground voltage stresses. The voltage where temperatures above 130 degrees centigrade may occur is limited to winding turn-to-turn stresses which do not exceed 80 volts.

Service Tests

A 500-kva 13,800-volt three-phase dry-type transformer was moved outdoors and given a rain test for 16 days with a water spray having a precipitation of 0.2 inch per minute applied continuously around the case. The case had screened openings in the bottom and the top. The transformer carried no load but was excited at 13,800 volts for 16 out of each 24 hours. During the period without excitation, the rain test was continued.

After this extreme test, lasting for 16 days, the transformer was subjected to the standard dielectric test of 31,000 volts from the high-voltage winding to the low-voltage winding and to ground for 60 seconds with no signs of distress whatever. While these dry-type transformers are intended only for indoor application where very little moisture is likely to be present, these results are reassuring when condensation or the effects of high humidity during installation and operation are considered.

A 150-kva 2,400-volt three-phase dry-type transformer, enclosed by an open-screened case, has been under test for more than 20 months, supplying power for the operation of motors and other equipment. The load cycle begins each week at 8:00 a.m. on Monday. The test is conducted in a relative humidity of approximately 95 per cent and at a room temperature varying from 32 to 50 degrees centigrade. At 4:00 p.m. each day, through Monday to Saturday, the load is removed but the transformer is kept energized and left to "soak" overnight. From 4:00 p.m. on Saturday until 8:00 a.m. on Monday, the transformer remains energized and the 95 per cent relative humidity is maintained. Under these extreme conditions this transformer has given continuous service and there are no indications of a deterioration of the varnish or of the insulation. The test is being continued.

Another dry-type transformer, enclosed by a sheet-steel case with louvers, having a normal rating of 480 volts, has been subjected to temperature and moisture tests for more than 2,000 hours, operating under the following cycle. The load was adjusted so that the average maximum continuous winding temperature, determined by thermocouples, is 175 degrees centigrade. The load is maintained for 16 of each 24 hours during five days of each week. At the end of each 16-hour load period, a water spray is directed against the transformer case for five minutes. Following this, the transformer is allowed to stand eight hours of each day without power applied. At the end of each weekly cycle, immediately following the last 16-hour load period, the standard dielectric test of 4,000 volts is made from the high-voltage winding to the low-voltage winding and to ground. Following this test, the water spray is applied for five minutes. The transformer then remains de-energized for 56 hours when power is again applied and the weekly test cycle is repeated.

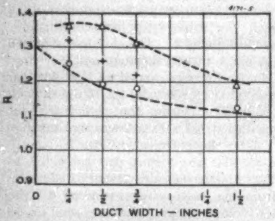


Figure 5. Variation of ratio of temperature rises

$$R = \frac{\text{hottest-spot temperature rise}}{\text{average temperature rise}}$$

O—Duct length 15 inches
+—Duct length 30 inches
Δ—Duct length 45 inches

A careful examination of the transformer after the 2,000-hour test period disclosed some deterioration of the varnish but no insulation failures have occurred nor were there any indications of a decrease in the dielectric strength of the insulation. The test is being continued.

Operating Experience

Many transformers of this type have been in continuous operation for more than seven years. Today, there are nearly 2,000 transformers in service totaling approximately 1,000,000 kva. Never has a breakdown of the winding insulation occurred in any of these transformers which could be directly attributed to an excessive hot-spot winding temperature, an accumulation of dirt, the effects of condensation, or operation in a humid atmosphere.

With nearly 2,000 transformers in operation, four transformers have failed in service. It is of interest to note the cause of these failures, the resulting damage to the windings of the transformers, and what was necessary to put the transformers back into service.

A 600-kva three-phase 6,900-volt dry-type transformer flashed over as the result of a severe lightning surge. The voltage impressed on the high-voltage winding was estimated to be in excess of 100 kv. Connected directly to the secondary of a large bank of power transformers, this dry-type transformer had no lightning protection of any kind. The high-voltage winding was severely damaged; the line leads and a considerable portion of the winding were fused because the system breaker permitted arcing for a considerable period of time. It is interesting to know that neither an explosion nor a fire occurred. It was necessary to replace the windings.

A 1,000-kva three-phase 13,800-volt dry-type transformer was temporarily put out of service because of a lightning surge. However, in this case, flashover occurred across two plain rectangular porcelain insulators which were used to support the high-voltage leads. As a secondary result, a small arc occurred at one point between the two outer turns of the top high-voltage coil section of one phase. The turn insulation was only slightly damaged. Permanent repair of the winding was made and the transformer was put back into service within two hours after the failure. Later, the rectangular-shaped porcelain insulators were replaced with round corrugated insulators.

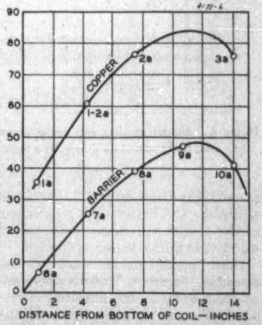


Figure 6. Typical curve of the temperature distribution in one section of a model winding assembly, showing the relation between the winding and the barrier temperatures for a one-half-inch duct width

Two other dry-type transformers failed after short periods of service. One failure was caused by a short-circuit between two adjacent turns in the high-voltage winding, apparently the result of a poor joint which punctured the insulation. Repair was effected easily and quickly. The other transformer outage was caused by the flashover of a porcelain lead support which may have been due to an excessive accumulation of conducting dirt. The only repair necessary was to replace the lead support.

There have been instances where a number of these dry-type transformers operated at overloads ranging from 150 to 200 per cent of rated load for continuous periods of time in excess of 24 hours. The windings must have been subjected to temperatures appreciably in excess of 160 degrees centigrade.

For instance, a bank of 150-kva 4,000-volt transformers carried in excess of 150 per cent of rated load for a period of nearly three days. This overload occurred nearly four years ago. A careful inspection of these transformers did not indicate any noticeable deterioration of the insulation. These transformers have continued to give uninterrupted service.

Two years ago, another bank of dry-type transformers carried approximately double load for more than 24 hours. These transformers have continued to operate without any ill effects of this heavy overload.

A 600-kva 13,800-volt three-phase dry-type transformer, after having been in service over one year, was operated at 200 per cent of rated load for a period of 30 hours. It is estimated that the maximum temperature of the windings was in excess of 225 degrees centigrade. No failure occurred and the transformer is continuing in service.

Conclusions

Because the major insulation of these dry-type transformers is primarily air, it is unaffected by temperatures within the operating range.

There are available special winding-conductor insulations which come within the scope of the present definition of class *B* insulation. These conductor insulations will withstand satisfactorily hottest-spot temperatures in excess of 130 degrees centigrade. Other parts of the insulation structure are subject to temperatures which are well within the established limits for the materials used.

Experience with dry-type transformers of this type operating under extremely humid conditions show that the effects of moisture are not a limiting factor.

Tests on these transformers, together with an operating record of more than seven years, give ample proof that these modern dry-type air-cooled transformers operating under normal as well as the usual abnormal conditions of service, may be expected to give continuous reliable service.

Bibliography of Relay Literature

1940–1943

AIEE RELAY SUBCOMMITTEE
Working Group on Relay Bibliography

THIS interim report was prepared by the working group on bibliography of the relay subcommittee of the AIEE committee on protective devices, in order to make immediately available this material to the entire industry. Previously it has been available only to the relay subcommittee in annual reports and then published at ten-year intervals. However, it was felt that the urgency of the relay problems brought about by the war emergency makes it desirable to have this information more generally available.

A "Bibliography of Relay Literature, 1927–1939" was published in pamphlet form in July 1941 and in the AIEE Transactions, Volume 60, 1941, pages 1435–47.

The articles listed in this bibliography (1940 to 1943) include all those appearing in the AIEE Transactions or Electrical Engineering as well as in most of the chief technical publications of the world from January 1940 through December 1943. These articles are subdivided into several sections. In each section the entries are numbered consecutively and listed alphabetically by years, the first significant word of the title determining the alphabetical position.

Section A. Line Protection—Distance

1942

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1943

2. Distance Relay Protection for Subtransmission Lines Made Economical, **L. J. Audlin, A. R. van C. Warrington.** AIEE Transactions, volume 62, 1943, September section, pages 574–8.

3. Distance Relaying With Low-Tension Potentials, **J. L. Blackburn.** *Westinghouse Engineer*, volume 3, May 1943, pages 61–4.

4. Performance Requirements for Relays on Unusually Long Transmission Lines, **F. C. Poage, C. A. Striefus, D. M. MacGregor, E. E. George.** AIEE Transactions, volume 62, 1943, June section, pages 257–83. Discussion, page 422.

5. Problem—Solution—Tests in Relaying 270-Mile 154-Kv Line. *Electric Light and Power*, volume 21, February 1943, pages 36–41.

6. Protective Relaying for Long Transmission Lines, **A. R. van C. Warrington.** AIEE Transactions, volume 62, 1943, June section, pages 261–8. Discussion, page 427.

Paper 44-104, recommended by the AIEE committee on protective devices for presentation at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944. Manuscript submitted April 10, 1944; made available for printing May 10, 1944.

Personnel of working group preparing this interim report: C. E. Parks, H. F. Lindemuth, S. Goldsmith, C. L. Smith, George Steeb.

Section B. Line Protection—Pilot Wire and Carrier Current

1940

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2. Carrier-Current Relaying Improved, **S. L. Goldsborough.** *Electrical World*, volume 113, January 1940, page 156.

1941

3. An Improved A-C Pilot-Wire Relay, **J. H. Neher, A. J. McConnell.** AIEE Transactions, volume 60, 1941, January section, pages 12–17. Discussion, page 638.

4. Possibilities and Methods of Extending Carrier-Current Relay Channel to Other Uses, **R. M. Smith, S. L. Goldsborough.** AIEE Transactions, volume 60, 1941, January section, pages 23–7. Discussion, page 645.

5. Powerton-Crawford 220-Kv Line—System Operating Features and Terminal Design, **H. E. Wulfing, T. G. LeClair.** AIEE Transactions, volume 60, 1941, pages 1084–9; discussion, page 1304.

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12. Relay Aids Use of 3-Terminal Lines. *Electrical World*, volume 118, August 22, 1942, page 638.

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Formulas for Calculating Short-Circuit Forces Between Channels Located Back to Back

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IN 1937 Arnold[1] published an analytic procedure enabling the designer of a single-phase two-conductor bus to determine combinations of conductor spacing and cross section that render the proximity effect zero—hence combinations that render the a-c resistance, and thus the eddy-current loss, a *minimum*. For explicit details the reader is referred to Arnold's paper. For our purpose it suffices to remark that in general the designer is afforded an indefinite number of combinations (of conductor spacing and cross section) from which to choose; that the exact cross sections determined by the theory are not simple in shape; that as the ratio of conductor spacing to perimeter of conductor cross section increases, the equivalent structural shape associated with a particular conductor spacing shifts from rectangular strap to channel to circular or square tubular conductor; that the a-c resistance, and hence the eddy-current loss, can be calculated from a given simple formula; and that the accuracy of this formula and the correctness of the general theory are verified by experimental data set forth in the paper.

Despite, however, the availability and the obvious usefulness of this general theory, it would seem that no considerable use, if any, has yet been made of it. In some part, no doubt, this neglect is due to lack of knowledge of it: Arnold's paper was published in the *Journal* of the Institution of Electrical Engineers, a British periodical which in this country enjoys but little circulation outside of engineering libraries or the technical departments of a few of the larger public libraries. In other part, however, this neglect doubtlessly can be attributed to the fact that, largely, a-c industrial-bus design is polyphase and has as its desideratum the achievement of minimum inductance, hence minimum reactance voltage drop. Designs achieving this end involve closely spaced conductors, thus greater than minimum eddy-current losses, whereas practical designs achieving minimum eddy-current losses involve fairly widely spaced conductors, thus increased reactance voltage drop.

In those instances, however, where single-phase busses are to be designed and minimum reactance is not so much the desideratum as is minimum eddy-current loss, Arnold's theory affords preferred designs. In consequence, it is of interest to the designer of such busses that there be available to him formulas enabling calculation of the electrical and mechanical performance of designs stemming from Arnold's theory. This, as already mentioned, yields conductor cross sections to be approximated by standard structural shapes: rectangular strap, channel, and circular or square tubular conductors.

Now formulas essential for calculating the a-c resistance, a-c inductance, mutual electromagnetic forces on short circuit, and other conductor parameters essential to knowledge of the electrical and mechanical (short-circuit) performance of the bus are available for rectangular strap and circular or square tubular conductors.[2] As regards (uniformly thick) channel conductors located back to back, Arnold has advanced a formula for calculating the a-c resistance and Higgins[3] has advanced a formula for calculating the mutual electromagnetic forces exerted between the conductors on short circuit. *But no formulas for the inductance have been published hitherto.* Yet, inasmuch as among the various combinations of conductor spacing and cross section afforded by Arnold's theory, that combination which affords the best all-around design is usually one comprised of moderately spaced channels placed back to back, it is most desirable to have formulas for the inductance. Such formulas have been derived by Schwantz and Higgins in a master-of-science thesis done by W. Schwantz under the supervision of T. J. Higgins, "Formulas for the Inductance of Busses Comprised of Channels Located Back to Back."

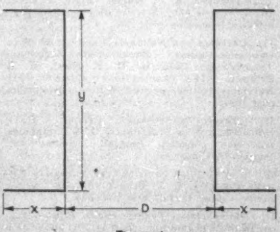

Figure 1

Subsequent to the derivation of and through the use of these formulas, the present writer has been able to derive new and improved formulas for calculating the mutual electromagnetic forces exerted on short circuit. These formulas—easier to use, more rapid in convergence, capable in general of greater accuracy than the earlier formula previously mentioned—are presented in this paper. Use of them considerably simplifies calculation of bus-bar support stresses.

The mechanical forces acting on bus-bar supports during short circuit are determined both by the magnitudes of the mutual electromagnetic forces exerted among the bus conductors and by the elastic properties of the supporting structure: its motional resistance, natural frequencies, and physical nature and arrangement. Comprehensive mathematical studies, substantiated by experimental data, of the individual and collective efforts of these factors and general methods for calculating the magnitudes involved have been advanced by Schurig and Sayre,[4] Dahlgren,[5,6] Eigermann,[7] Pilcher,[8] and Aebi.[9] In shorter articles based on the work of the first-named Schurig, Fricke and Sayre[10]—Tanberg,[11] Specht,[12] Edson,[13] and others[14] have presented certain charts, nomograms, and short cuts that facilitate the numerical labor incident to actual calculation. In consequence of the work of these men, it may be taken that if the designer has knowledge of the physical make-up of the bus structure and of the mutual electromagnetic forces exerted among the bus conductors, he can readily calculate the forces on the bus supports. Obviously, knowledge of the bus structure is immediately available to the designer. Accordingly, if there are also available to him formulas for calculating the mutual electromagnetic forces, he has all that is essential to calculation of the bus support stresses.

Such formulas are derived in this paper, it being assumed that the conductors are nonmagnetic and of such length that end effects are negligible and carry currents distributed uniformly over the cross sections, and that these cross sections are so thin they may be considered to be comprised of line segments (Figure 1).

Of these four assumptions the first and second are commonly satisfied in practice. Re the third, though skin and proximity effects are always present on a-c busses (however, the latter can be largely annulled by use of Arnold's analysis) at power frequencies and normal conductor spacings, their effect on the magnitudes of the electromagnetic forces is, as Beetz[15] has discussed at length, vanishingly small: whence this assumption introduces error negligible with reference to the accuracy

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required in calculating short-circuit forces and enables solution of an otherwise intractable problem. Finally, the assumption of negligible thickness affords formulas that yield values substantially identical with those calculated from the previously mentioned earlier-derived formula for uniformly thick conductors and yet require but a tithe of the numerical computation—whence the labor involved, the time required, and the possibilities of numerical error are considerably reduced.

The desired formulas obtained—by simple differentiation of the known formula for the inductance of the bus with cross section as in Figure 1—their use is illustrated by the numerical solution of a typical problem in bus design.

Derivation of the General Formulas

As derived by Schwantz and Higgins, the inductance per unit length of a circuit comprised of two identical channels located back to back as in Figure 1 is

$$L=-(y+2x)^{-2}\{2[y^2-(D+2x)^2]\log[y^2+(D+2x)^2]-4\{[y^2-(D+x)^2]+2y(D+x)\}\times \log[y^2+(D+x)^2]+8yD\log(y^2+D^2)-4[(y^2-x^2)-2xy]\log(y^2+x^2)-4(D+2x)^2\times \log(D+2x)+8(D+x)^2\log(D+x)+12y^2\log y-8D^2\log D+8x^2\log x+8y(D+2x)\tan^{-1}(y/D+2x)-8\{2y\times(D+x)+(D+x)^2-y^2\}\tan^{-1}(y/D+x)-8(y^2-D^2)\tan^{-1}(y/D)-8(y^2+2xy-x^2)\times \tan^{-1}(y/x)+4\pi y(y+2x)\} \quad (1)$$

From fundamental electromagnetic theory, the energy W in the magnetic field associated with a unit length of the circuit when it carries current I is

$$W=LI^2/2 \quad (2)$$

and the mutual electromagnetic force F exerted between the conductors is

$$F=\partial W/\partial D \quad (3)$$

Substituting equation 1 in equation 2, substituting the resulting expression in equation 3, performing the indicated differentiation, and collecting terms yields

$$F=-4I^2(y+2x)^{-2}[-{}^1/_2(D+2x)\log[y^2+(D+2x)^2]+(D+x)\log[y^2+(D+x)^2]-y\times \log[y^2+(D+x)^2]+y\log(y^2+D^2)-(D+2x)\log(D+2x)+2(D+x)\log(D+x)-2D\log D+y\tan^{-1}(y/D+2x)-2(y+D+x)\tan^{-1}(y/D+x)+2D\times \tan^{-1}(y/D)] \quad (4)$$

Simplicity and increased accuracy of numerical computation stem from combining the logarithmic terms according to the rules for addition and subtraction of logarithms. In such manner we obtain from equation 4

$$F=-4I^2(y+2x)^{-2}[(D/2)\times \log[y^2+(D+2x)^2](D+2x)^2D^{-4}+(D+x)\times \log[y^2+(D+x)^2](D+x)^2[y^2+(D+2x)^2]^{-1}(D+2x)^{-2}-y\log[y^2+(D+x)^2](y^2+D^2)^{-1}+y\tan^{-1}(y/D+2x)-2(y+D+x)\tan^{-1}(y/D+x)+2D\times \tan^{-1}(y/D)] \quad (5)$$

Finally, if we take $a=D/y$, $b=(D+x)/y$, and $c=(D+2x)/y$, equation 5 can be written

$$F=2kI^2/D \quad (6)$$

wherein k is a dimensionless parameter to be calculated either from

$$k=[-2Dy/(y+2x)^2][(a/2)\times \log[c^2(1+c^2)/a^4]+b\log[b^2(1+b^2)\div c^2(1+c^2)]-\log[(1+b^2)/(1+a^2)]+\tan^{-1}(1/c)-2(b+1)\tan^{-1}(1/b)+2a\times \tan^{-1}(1/a)] \quad (7)$$

or

$$k=[-2Dy/(y+2x)^2][(a/2)\times \log[c^2(1+c^2)/a^4]+b\log[b^2(1+b^2)/c^2\times(1+c^2)]-\log[(1+b^2)/(1+a^2)]-\tan^{-1}c+2(b+1)\tan^{-1}b-2a\times \tan^{-1}a+\pi(a-b-0.5)] \quad (8)$$

equation 8 being obtained from equation 7 by use of the trigonometric identity, $\tan^{-1}X+\tan^{-1}(1/X)=\pi/2$.

Alternatively, if we take $A=y/D$, $B=(D+x)/D$, and $C=(D+2x)/D$, equation 5 can again be written in the form of equation 6, but therein k is now given by

$$k=[-2D^2/(y+2x)^2][{}^1/_2\log(A^2+C^2)\times C^2+B\log[B^2(A^2+B^2)/C^2(A^2+C^2)]-A\times \log[(A^2+B^2)/(A^2+1)]+A\tan^{-1}(A/C)-2(A+B)\tan^{-1}(A/B)+2\tan^{-1}A] \quad (9)$$

or

$$k=[-2D^2/(y+2x)^2][{}^1/_2\log(A^2+C^2)\times C^2+B\log[B^2(A^2+B^2)/C^2(A^2+C^2)]-A\times \log[(A^2+B^2)/(A^2+1)]-A\tan^{-1}(A/C)+2(A+B)\tan^{-1}(A/B)-2\tan^{-1}A-\pi\times(0.5A+B-1)\} \quad (10)$$

Inasmuch as a, b, c, A, B, C, and k are dimensionless, we note that in calculating these quantities x, y, and D may be measured in any unit of length; but otherwise the units of all quantities mentioned to this point are in the absolute system: x, y, and D in centimeters; the current I in abamperes; L in abhenrys per centimeter of bus length; W in ergs per centimeter of bus length; F in dynes per centimeter of bus length.

Inasmuch as in actual practice the dimensions x, y, and D commonly are stated in inches, the current I in amperes, and the force F in pounds per foot, it is convenient re numerical computation to have an equation equivalent to equation 6 wherein, however, the various quantities are measured in the units just stated. Accordingly, introducing the necessary conversion factors in equation 6, we obtain

$$F=(5.4kI^2/D)10^{-7} \text{ pounds per foot of bus length} \quad (11)$$

wherein k, as before, is given by equations 7, 8, 9, or 10.

Regarding these formulas for k, we may remark that though each involves calculation of the sum of a number of logarithmic and arctangent terms—six in all—the actual labor required is not heavy. Thus, using the 15-place tables of natural logarithms and arctangents (with 0.0001 and 0.001 increment arguments) published by the Work Projects Administration[16] and effecting numerical calculations on a ten-digit electric calculating machine, the writer was able to effect the computation pertinent to the following illustrative example in but a few minutes.

An Illustrative Example

The conductors of a single-phase bus are standard aluminum channels (web, $y=8$ inches; flange width, $x=2.56$ inches; thickness 0.524 inches) spaced back to back, $D=16$ inches. To calculate k:

By virtue of the given data we have: $a=2$, $b=2.34$, and $c=2.64$. Substituting these values in equation 8 and performing the indicated calculation yields

$$\begin{aligned}k&=-(1/0.6724)[\log 3.4715578-2.32\times \log 1.6169046-\log 1.27648-\tan^{-1}2.64+6.64\tan^{-1}2.32-4\tan^{-1}2-0.82\pi]\\&=-(1/0.6724)[1.2446034-2.32\times(0.4805136)-0.2441063-1.2087089+6.64(1.1638255)-4(1.1071487)-0.82(3.1415927)]\\&=-(1/0.6724)[-0.5999026]\\&=0.892+\end{aligned}$$

Alternatively, $A=0.5$, $B=1.16$, and $C=1.32$. Substituting these values in equation 9 and performing the indicated calculation yields

$$\begin{aligned}k&=-(1/0.3362)[0.5\log 3.4715578-1.16\times \log 1.6169046-0.5\log 1.27648+0.5\tan^{-1}0.3780303-3.32\times \tan^{-1}0.431034+2\tan^{-1}0.5]\\&=0.892+\end{aligned}$$

Obviously, since it is only desired to know k to two or at the most three decimal places, it is not necessary to use seven-place logarithms and arctangents. Inasmuch, however, as 15-place tables were employed and the calculation was done on a ten-digit calculating machine, the added labor is negligible, and one has no uncertainty as to the effect of cumulative errors on the accuracy of the first few digits of k.

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The Nature of Vibration in Electric Machinery

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Synopsis: Vibration in electric machinery is the result of a number of contributing agencies, some mechanical, others electromagnetic in origin. Features of mechanical and electrical design and construction which give rise to these agencies are discussed. The general character of the vibration which each agency produces is explained, as well as how different agencies combine to produce the resultant machine vibration. Where excessive vibration is encountered in the field, methods are outlined for ascertaining the major agencies responsible so that corrective modifications may be made which will be effective in reducing the resultant vibration. These methods are applied to motor-generator sets driven by two-pole induction motors to ascertain the cause of a particular type of vibration characterized by the vibration amplitude increasing and decreasing in uniform cycles twice during each revolution of motor slip. In this way the agencies which give rise to this particular type of vibration are explained, and methods by which they may be controlled to keep the resultant vibration to a satisfactory minimum are discussed.

DURING the past few years the progress made in reducing vibration in electric machinery has resulted in more freedom from mechanical failures and other maintenance troubles. The more quiet machines which result from reduced vibration also produce less nervous strain to the operators. The generally accepted theory of vibration is based on the premise that vibration and noise arise from forces which act in a periodic manner upon the different component parts of the machine. Such forces are called "driving forces," and the actual mechanical displacement or vibration which results is frequently called the "vibrational response." The amplitude of the vibrational response depends upon the amplitude of the driving forces as well as upon the mass, stiffness, and damping factors associated with the vibrating member. While this fundamental concept is quite simple, yet to account for particular types of vibration which are produced in electrical equipment and mechanical rotating assemblies is sometimes quite difficult.

In general, the amplitude of a vibrational response can be reduced either by decreasing the amplitude of the driving forces which cause it, or else by modifying the mass, stiffness, and damping factors associated with the vibrating member so that a smaller response occurs under the action of the same driving forces. In electric machinery there are many agencies which contribute to these factors and to the final vibrational response. As a rule it is not a simple process to isolate the particular agencies responsible for excessive vibration, particularly when several agencies are cumulative in effect and are present simultaneously. A careful systematic study based upon a knowledge of conditions which are conducive to excessive vibration and the frequency components which arise from them usually is necessary before effective corrective measures can be specified.

The driving forces which are present in electric machinery are of two general kinds; namely, mechanical and electromagnetic. Mechanical driving forces are those which arise from mechanical or structural irregularities such as mechanical unbalance, nonsymmetrical mechanical parts, friction, loose bearings, and so forth. Electromagnetic driving forces are those which arise by virtue of magnetostriction and magnetic attraction between different parts of the machine. In any electric machine both kinds of driving forces are present simultaneously and act together to produce the vibrational response.

A very important agency which contributes to excessive vibration is static or dynamic unbalance of a rotating assembly. This produces an excessive mechanical driving force of fundamental frequency—that is, numerically equal to the speed of rotation in revolutions per second—and this results in an excessive vibrational response of the same frequency. This phenomenon has long been realized and many kinds of balancing machines, some of which are quite elaborate, have been developed for testing and correcting the condition of balance of rotating parts. For this reason this factor is not considered in detail here. It is mentioned merely because other types of vibration which arise from altogether different agencies frequently are attributed to improper balance. Mechanical unbalance alone always gives rise to a vibrational response having a frequency which is numerically equal to the speed of rotation in revolutions per second. This fact very often can be used to differentiate between excessive vibration due to mechanical unbalance and excessive vibration due to other agencies. Components of vibration having frequencies which are multiples of the speed of rotation in revolutions per second cannot be reduced appreciably by improvement in the conditions of balance of rotating parts, because the driving forces which produce them originate through other agencies.

Another well-known cause of excessive vibration which must be dealt with in machine designs is closely related to mechanical resonance in the rotating assembly and is referred to as the phenomenon of ordinary critical speeds. When the speed of rotation is such that the frequency of the driving force due to mechanical unbalance corresponds with one of the natural resonant frequencies of the rotating assembly, the latter is in indifferent equilibrium, and a small component of driving force may cause excessive vibration and whipping, or even mechanical failure, of the rotating assembly. For satisfactory operation the ordinary critical speeds must be consider-

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ably outside the normal operating speed range of the machine. This subject has received careful attention by many investigators who have developed methods whereby the critical speeds may be computed from the dimensions and component masses of the assembly.

Mechanical Driving Forces

The fundamental component of driving force which arises from a rotating assembly is caused by static or dynamic unbalance. It has a frequency numerically equal to the speed of rotation in revolutions per second. The amplitude of this driving force may be controlled by careful adjustment of balance of the rotating parts. In addition to the fundamental component, harmonic driving forces having frequencies which are multiples of the speed of rotation in revolutions per second frequently occur. These are caused by mechanical imperfections or dissymmetries in the construction of the machine. For this reason they are more difficult to control than the fundamental. The second harmonic component—that is the one having a frequency double that of the fundamental—is most likely to give rise to troublesome vibration. This component arises from shafts having portions of noncircular cross-sectional area, caused by keyways, for instance, from slightly elliptic bearings, as well as from other mechanical imperfections. Elliptic bearings are more likely to be a source of trouble in the case of ball bearings as avoidance of slight warping of the bearing races is practically impossible during the heat-treatment processes necessary in their manufacture. Slightly oval or nonspherical balls also may cause harmonic driving forces; but these are usually of higher harmonic order than the second and are not necessarily integral multiples of the fundamental. The only effective method by which harmonic driving forces arising from these causes may be controlled is by the use of rotating assemblies and shaft designs which are as nearly circular in cross section as practical considerations will permit, and by better workmanship and closer tolerances in the manufacture of the component parts of the machine. Harmonic driving forces must be given special consideration from the standpoint of vibration and noise in machines of special design where the rotating assembly is unusually long if the shaft has a keyway or other dissymmetries. They may be serious also if the inherent resonant frequencies of the machine frame correspond closely with the frequency of the harmonic driving forces.

Electromagnetic Driving Forces

Electromagnetic driving forces arise in electric machinery for several reasons, but the principal fundamental causes may be traced to magnetostriction and magnetic attraction between different parts of the magnetic circuit of the machine. Magnetostriction plays an important role in creating vibration and noise in static apparatus such as transformers. In rotating machinery the forces caused by magnetic attraction are by far the more important. In general these forces are quite complex in nature, consisting of numerous harmonic components. The frequency of the most important component, at least with respect to amplitude, is twice the frequency of the voltage at the machine terminals, and for this reason will be called the second harmonic component of electromagnetic driving force. Other harmonic components of driving force arise because of the slots in the stator and rotor of the machine. The order of these harmonic components is a function of the number of slots on both the stator and the rotor. Certain of these are attributed to the machine windings not being infinitely distributed, while others exist simply because of the stator and rotor teeth. Certain combinations of stator and rotor slots result in much more pronounced driving forces than do others, and methods have been developed for choosing the optimum combinations for minimum noise. As a rule these harmonic components are of comparatively high order and are of importance in vibration principally from the standpoint of noise.

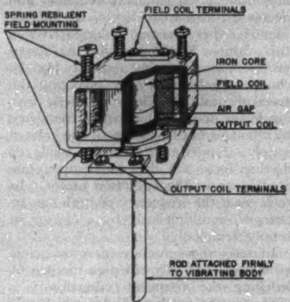

Figure 1. Electrodynamic-type vibration pickup unit

The second-harmonic component of electromagnetic driving force always is present in an electric machine. It arises principally from the radial magnetic attraction between the stator and rotor. Nearly all rotating machines make use of revolving magnetic fields. As the magnetic field revolves in the air gap of the machine, it passes a given point of the stator twice during each cycle of voltage change, once as a flux entering the stator and once as a flux entering the rotor. A force of attraction results regardless of the flux polarity. This force therefore builds up and dies away twice during each cycle of voltage change and produces a driving force having a frequency exactly twice that of the voltage supply. This driving force causes a periodic distortion of the stator core which, through the agency of the stator mounting supports, acts as a driving force to excite vibrations in the frame of the machine. This driving force cannot be eliminated since it is inherent in any electric machine, but the vibration or response it causes can be controlled by constructing the stator core sturdy enough so that the resultant core response is small, or in special cases such as large alternators (15,000 kva and larger) by mounting the stator core on flexible supports so that minimum vibration is transmitted to the frame of the machine.

The second-harmonic component of driving force is much more pronounced in two-pole machines than in machines having a greater number of poles, because the distance along the stator periphery between points where the flux is simultaneously at maximum values is comparatively much greater. This is one of the fundamental reasons why two-pole motors present more difficult problems from the standpoint of vibration than do motors having a greater number of poles. Forces of magnetic attraction act simultaneously upon diametrically opposite points of the stator and cause it to become distorted into an elliptic form, the distortion progressing around the stator in synchronism with the magnetic field.

In addition to the factors already mentioned, harmonic driving forces result from air-gap dissymmetries. The type of dissymmetry most frequently encountered is imperfect centering of the rotor in the stator core. This gives rise to a second-harmonic electromagnetic driving force in a two-pole machine. As the magnetic flux in the air gap revolves, it passes the point where the air gap is shortest twice during each cycle of voltage change, once as a flux entering the rotor and once as a flux entering the stator. Simultaneously, a flux of opposite polarity exists in the air gap at the point diametrically opposite. Since the radial magnetic force exerted on the rotor increases as the air-gap length decreases, the two attractive forces acting at diametrically opposite points of the rotor do not always cancel. When the flux is in the position where the air gap is longest or shortest, a net force results which acts toward the point where the gap is shortest. As the air-gap flux moves from the position, the unbalance between the two diametrically opposite forces diminishes and becomes zero at the point 90 degrees from this position. As the flux rotates further, an unbalance again exists in the same direction and reaches a maximum when the flux has moved through another 90 degrees. This unbalance of radial forces builds up and dies away twice during each cycle of voltage change and produces a second-harmonic driving force which acts to deflect the rotor toward the point where the air gap is shortest. This driving force is transmitted through the bearing supports and serves as a driving force to produce vibrations in the frame of the machine.

This driving force may be eliminated by perfect centering of the rotor within the stator core. It also may be used in some instances to counteract or neutralize driving forces of the same frequency which arise from other causes, since the phase and magnitude of the driving force thus produced can be controlled by the use of an adjustable bearing support which will allow the rotor to be shifted off center in any desired direction. The magnitude of this driving force depends upon the amount by which the rotor is off center with respect to the stator, and the phase (with respect to the impressed voltage) depends upon which direction it is off center, that is, upon the angular position of the point in the air-gap periphery where the gap is shortest.

The use of this method for controlling vibration has several disadvantages from the standpoint of actual practice. Each machine of a like series must be adjusted independently to secure the maximum benefit. Also, if the machine is at all critical with respect to its mounting, an adjustment correctly made at the factory or for one location in service would not be correct for any other location or mounting. This method can be used to advantage, however, in emergencies and other instances where a better method cannot be readily applied.

Combined Effect of Electromagnetic and Mechanical Driving Forces

During the operation of an electric machine, the vibration or response which results from several driving forces may be considered from the standpoint of superposition. Each harmonic component of driving force will produce a component of response of the same frequency as that of the driving force. The amplitude of each component of the response depends upon the magnitude of the driving force and upon the mass, stiffness, and damping factors associated with the component parts of the machine. The resultant total response at any point of the frame is equal to the sum of the harmonic components of the response. If there is a correspondence in frequency between one or more of the driving forces and the natural or resonant frequencies of the component parts or complete assembly of the machine, comparatively large amplitudes of vibration will result, consisting principally of frequencies for these particular modes of vibration.

As a rule, excessive vibration due to resonance can be recognized by the behavior of the vibration accompanying changes in frequency of the driving forces. In the neighborhood of resonance, the vibration will build up to a maximum amplitude at the resonant frequency and decrease as the frequency of the driving forces is either increased or decreased from this value. Mechanical driving forces produced by a rotating assembly may be changed in frequency by changing the speed of rotation. Electromagnetic driving forces may be changed in frequency by changing the frequency of the voltage supply. Sometimes the two kinds of driving force are dependent upon one another as in the induction motor. In such cases the frequency of both can be changed simultaneously by a change in supply frequency.

In cases where mechanical resonance is prominent, the only effective method of reducing the resultant vibration is a change in the machine design which will shift the natural resonant frequencies inherent in the machine assembly outside the range of frequencies encountered during normal operation so that mechanical resonance will not occur. To insure this result in a new design is not easy because the geometric shapes of machine frames and other parts do not lend themselves to accurate mathematical analysis. It is very difficult, except in special cases, to compute the natural resonant frequencies of a machine frame or assembly. The final test of a design is the nature of the vibrational response which results under the action of the driving forces present under normal operation of the completed machine. If high amplitudes of vibration do not arise from small driving forces, the design is satisfactory. Otherwise stiffening of certain parts and modifications in the shape of others are necessary. Additional stiffness may be secured by increasing the size of the members, or by the use of cross braces of one form or another which increase the rigidity. The use of steel of higher tensile strength will have little or no effect, since the stiffness depends upon the modulus of elasticity which is essentially the same for all grades of steel regardless of the ultimate tensile strength.

From the standpoint of excessive vibration which results in excessive bearing wear, failure of mechanical members, and other maintenance troubles, as contrasted with noise, frequencies in the lower audio range are of major importance. For the lower resonant frequencies the stiffness and damping factors of mechanical members are comparatively much smaller, and a much greater resonant response can result. Fortunately, very low resonant frequencies are not usually encountered in actual practice in machines of standard design, because the frame and other parts must be constructed with sufficient strength to withstand stresses which are likely to arise during both normal and abnormal conditions of operation. For this reason the natural resonant frequencies inherent in the assembly usually will be of comparatively high order as compared with the driving forces produced by the machine in operation. Exceptions to this general rule occur in very large machines, and in compact high-speed motor-generator sets having comparatively long armatures. This is particularly true in machines of fabricated-steel-frame construction. In this latter instance considerable rigidity is sacrificed for the lighter construction thus

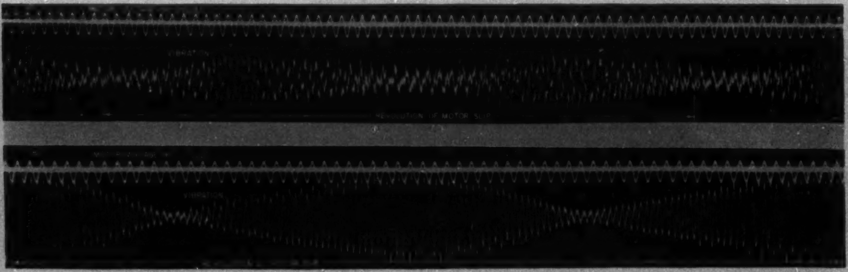

Figure 2. Vibration of motor generator set under load with voltage at rated frequency applied to the motor

A. Actual vibration as recorded by means of electrodynamic vibration pickup unit

B. Same as A except high-frequency components filtered out of recorded wave afforded, and the lowest natural frequency may be of the order of 120 cycles per second or even less.

In the case of two-pole 60-cycle induction motors, the important electromagnetic driving force of lowest frequency is 120 cycles per second. At the same time a mechanical driving force, the second harmonic component, which has a frequency double the speed of rotation in revolutions per second, can exist. When these two driving forces exist simultaneously, they alternately reinforce and interfere with each other and cause a response which builds up and dies away in amplitude giving rise to the phenomenon of beats. Since the difference in frequency is exactly twice the slip frequency of the induction motor, two cycles of the beat frequency correspond exactly with one revolution of motor slip. If the lowest resonant frequency of the machine frame is in the neighborhood of 120 cycles per second, the vibration amplitude may build up to an excessively high value even though the driving forces are comparatively small. The beats are then very pronounced and clearly audible. The only effective manner of reducing vibration in this case is to modify the machine frame to avoid mechanical resonance, and perhaps also to modify the machine construction to reduce the amplitude of the driving forces. If the machine is of a portable nature such that the method of mounting or arrangement of support on its foundation may be changed, or if the machine operates over a fairly large speed range, the frame would have to be made more rigid, rather than less rigid, in order to avoid possible operation in the resonant range. Increasing the rigidity has the effect of raising the resonant frequencies. Appreciable increase in rigidity cannot be obtained by the use of the same frame design and steel of higher tensile strength. Frame members of higher moment-of-inertia cross section must be used.

Experimental Studies and Observations

The ideas presented in this paper are the result of considerable research which has been conducted on motor-generator sets in which the complete rotor assembly of both motor and generator was mounted on a single shaft supported by two ball bearings, one at either end of the shaft. The complete set was housed by a single fabricated steel frame of welded construction. The driving motor was a 3,600-rpm two-pole squirrel-cage induction motor. The particular type of vibration produced in these sets was characterized by the amplitude of the vibration building up and dying away twice during each cycle of motor slip, thus producing a very decided beat which was clearly audible. The beat is clearly distinguishable in most of the oscillograms of the accompanying figures which were obtained by means of an electrodynamic vibration pickup unit. The signals so obtained were amplified by one stage of voltage amplification, followed by a push-pull power-amplifier stage, the output of which was recorded by means of a mechanical oscillograph.

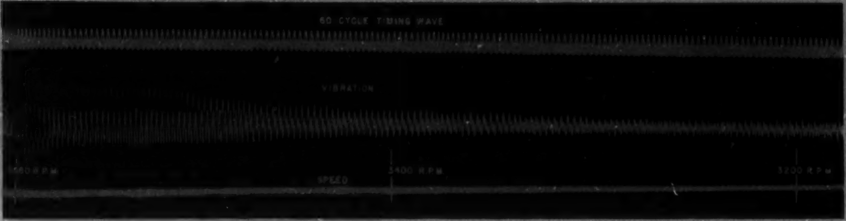

Figure 3. Vibration of motor generator set coasting to rest without load and without generator excitation

Figure 1 is a pictorial drawing of the pickup unit. The field coil was excited with direct current to produce a flux in the air gap in which the small output coil was mounted. The field coil and its associated magnetic circuit were supported on comparatively weak springs to allow free motion between this assembly and the output coil. The small frame which carried the output coil was rigidly attached to or else firmly held against the mechanical member when the vibration of the member was being recorded. The principle of operation of the pickup unit is similar to that of the dynamic type microphone, except that it is mass controlled. The electromotive force generated in the output coil is proportional to the relative velocity of this coil with respect to the field-coil magnetic circuit. The output wave form represents the instantaneous velocity of the vibrating member.

The actual mechanical displacement can be obtained from the velocity wave by taking the derivative, or else by resolving the wave into its sinusoidal components, dividing each component by its own angular frequency, combining the resultant terms, and interpreting the wave to the proper scale. As far as actual mechanical displacement is concerned, the velocity wave accentuates the high frequencies. If the noise produced by a machine is the factor of principal concern, the instantaneous sound pressure is more closely related to the instantaneous acceleration. The acceleration wave may be obtained from the velocity wave by one step of integration, or else by taking the sinusoidal components of the velocity wave, multiplying each by its own angular frequency, combining the resultant terms, and interpreting the resultant wave to the proper scale. The velocity wave represents an average of two extremes, and thus forms a good basis for comparing relative vibration amplitudes. It may be used very satisfactorily in studying the effects of different factors on the amplitude and nature of the vibration produced in a machine.

In order to eliminate the effects of stray electromagnetic fields which were very prominent in the proximity of the machines used in this investigation, the vibration pickup unit and the amplifier had to be very carefully shielded, both electrostatically and electromagnetically. The power amplifier stage was of the transformer input type and presented a difficult problem from this standpoint, because of the transformer connected to the grids of the output tubes. To reduce stray pickup from this source to a negligible value, the transformer was enclosed in a $^{3}/_{16}$-inch-thick soft-steel shield, and the amplifier was placed several feet from the machine under test.

Figure 2A is a representative oscillogram of the actual vibration which was obtained by the use of this apparatus. Careful examination of the pattern shows that the wave consists principally of a component of slightly less than 120 cycles per second which varies in amplitude having two maximum and two minimum values for each revolution of slip of the induction motor. In addition, the wave contains numerous harmonic components, some of rather high order, which are of fairly constant amplitude and bear no apparent relationship to the slip of the motor. The frequency of several of these harmonic components was measured with a sound analyzer and found to check with the frequencies to be

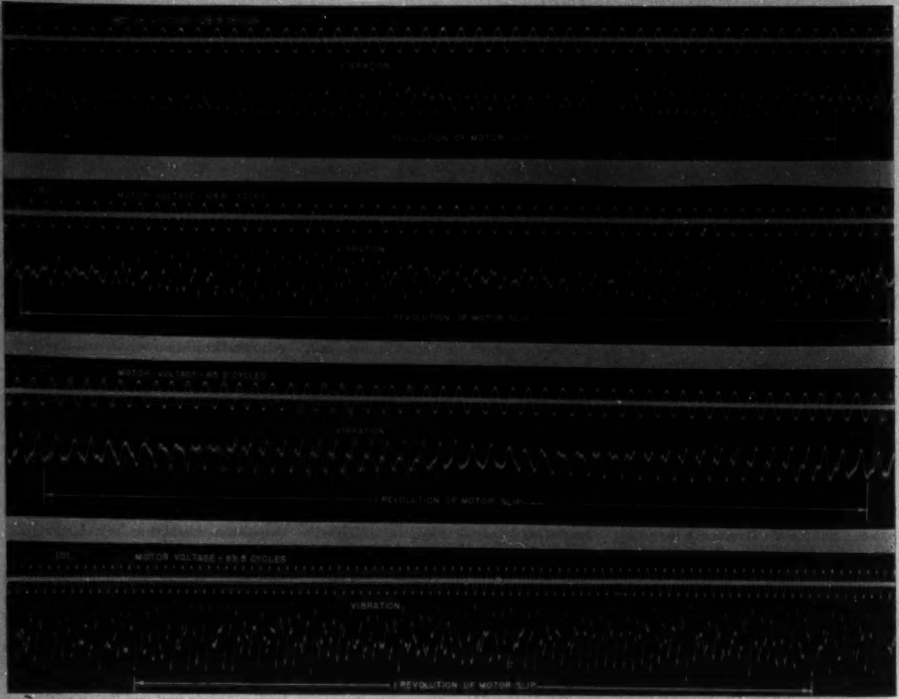

Figure 4. Oscillograms showing the effect upon the machine vibration of simultaneous changes in the motor supply frequency and speed

A. Machine speed 3,400 rpm
B. Machine speed 3,800 rpm
C. Machine speed 3,900 rpm
D. Machine speed 5,000 rpm
For comparing vibration amplitude of D with amplitudes of A, B, and C, divide by two

expected from the stator and rotor teeth of the machine. For these reasons, a capacitor was connected across the output terminals of the voltage amplifier stage to by-pass the high-frequency components and filter them out of the recorded wave. Figure 2B is an oscillogram of the same vibration recorded in Figure 2A, but with the filter capacitor connected into the amplifier circuit. Beats in this oscillogram are very pronounced, showing that the wave contains two principal components of approximately equal amplitude. By analysis, the frequency of one component is exactly 120 cycles per second, and the frequency of the other is exactly double the speed of rotation of the motor in revolutions per second.

The presence of the 120-cycle component of vibration is easily explained on the basis of electromagnetic driving forces produced in the machine as previously described. The component having a frequency twice the speed of the motor is due to mechanical causes only. This is shown clearly by the oscillogram of Figure 3. This oscillogram was obtained by starting the motor without load and allowing it to reach maximum no-load speed. Then the motor voltage supply was switched off and the machine allowed to coast to rest without load and without electrical excitation of any kind. The vibration was recorded while the machine was coasting to rest. The mechanical vibration has a maximum amplitude at approximately 3,580 rpm and contains a very large second harmonic component. This component decreases in amplitude quite markedly as the speed decreases, and becomes negligible at approximately 3,300 rpm.

That the beat vibration reached a high amplitude because of mechanical resonance is demonstrated by the following test. The motor of the set was supplied from an adjustable frequency source and operated at different speeds by adjustment of the source frequency. The beat would become discernible when the speed was approximately 3,300 rpm, increase in intensity with an increase of speed above this value, and reach a maximum intensity at approximately 3,560 rpm. As the speed was increased further, the beat would come less prominent, and would be barely discernible at 3,900 rpm. It was completely absent at 5,000 rpm. This behavior is illustrated by the oscillograms of Figure 4, together with that of Figure 2B. These oscillograms were obtained at speeds of 3,400 rpm, 3,800 rpm, 3,900 rpm, 5,000 rpm, and 3,530 rpm, respectively. For a comparison of relative amplitudes in Figure 4, the ordinates of oscillogram D are to be divided by two, since this oscillogram was obtained with the oscillograph adjusted for double the sensitivity used for recording the other three.

To ascertain the part of the machine responsible for resonance, the rotor assembly first was examined critically. The first ordinary critical speed was computed analytically by several methods from the dimensions and masses of the rotor assembly. Different methods gave values ranging from 5,500 rpm to 6,200 rpm. As a final check, the rotor assembly was removed from the machine and mounted on rigid supports at the bearings. The natural frequency of vibration of the rotor assembly was measured by recording the free vibration of the rotor assembly under impact. Figure 5 is an oscillogram of the vibration at the center of the rotor assembly caused by a light blow from a rubber hammer. The blow was applied at the quarter-point of the rotor assembly. The frequency of the free vibration is 104 cycles per second, which corresponds with a first ordinary critical speed of 6,240 rpm. In actual operation of this machine, the bearings were not rigidly supported. This lack of complete rigidity somewhat lowered the critical speeds. By observation, the secondary critical speed due to gravity actually occurred between 2,800 and 2,900 rpm, which would fix the first ordinary critical speed at approximately 5,700 rpm. With the first ordinary critical speed of the rotor in this speed range, it is impossible to attribute the resonance phenomenon which occurred at 3,560 rpm to any resonant condition in the rotor assembly.

Impact tests similar to those described above were made to determine resonant frequencies in the machine frame. The complete frame, which was rectangular in shape, was mounted on three rigid point supports which could be moved to different relative positions. Blows were applied at different points of the frame, with and without the rotor assembly in place; and the vibration resulting at various points of the machine was recorded and studied. The nature of the free vibration depended upon the relative location of the three fixed point supports, the point where the blow was applied, and the point where the vibration was recorded. It made no apparent difference whether the rotor was in place or removed. The oscillograms of Figure 6 and Figure 7 are typical of the results obtained. Figure 6 shows a very prominent 115-cycle component and Figure 7 a very prominent 125-cycle component. In obtaining the oscillogram of Figure 6, two of the three point supports were placed at extreme corners of the frame at one end of the machine, and the third point support was placed at the center of the opposite end. For Figure 7, symmetry of the supports with respect to the frame was maintained, but the supports were placed considerably closer together. The actual mounting of the machine corresponded to a condition intermediate between these two and gave a resonant frequency of approximately 119 cycles per second.

As a final check on considerations regarding the cause and nature of the beat vibration, the frame of the machine was stiffened by welding several pieces of angle iron to the frame. The machine was operated at rated voltage and frequency, and the vibration was studied under different conditions of motor load, and with various arrangements of the three rigid frame supports. After reinforcement of the frame, the beat was completely absent at many points of the frame regardless of the arrangement of the rigid frame supports. At other points the beat could be detected, but was not prominent. At points where the beat was most prominent, the vibration was similar in character to that of Figure 4C. At other points, the vibration consisted almost entirely of a component slightly less than 60 cycles per second, which was due to slight mechanical unbalance of the rotating assembly. That the beat vibration was produced by the combined effect of electromagnetic and mechanical driving forces and reached a high amplitude because of resonance in the frame is clearly indicated by these tests and observations.

Conclusions

Vibration and noise in electric machinery arise by virtue of both mechanical and electromagnetic driving forces which exist simultaneously during normal machine operation. The amplitude of the vibrational response which results depends upon the amplitude of the driving forces, and also upon the mass, stiffness, and damping factors of the component parts of the machine. To secure quiet operation and comparative freedom from vibration, all of these factors must be dealt with. When excessive vibration is present in a machine of a particular design, the factors or agencies which contribute most to this condition must be ascertained, and modifications in design and construction made to minimize them. Because of inherent design features, two-pole induction machines and high-speed motor generator sets present more difficult problems from the standpoint of vibration than do machines having a greater number of poles which operate at lower speeds. Certain mechanical and structural irregularities of the rotating assembly may produce harmonic driving forces, and in particular a mechanical driving force having a frequency which is double the speed of rotation in revolutions per second. This driving force, acting simultaneously with an electromagnetic driving force having a frequency double that of the voltage supply, causes a resultant vibrational response which builds up and dies away in amplitude twice during each revolution of motor slip. This "beat vibration" may be very troublesome, particularly if the frame of the machine has an inherent resonant frequency which is approximately double the frequency of the voltage supply. A beat vibration of this kind may be reduced by careful attention to mechanical irregularities which give rise to the second harmonic component of mechanical driving force, or, if resonance is a contributing factor, by modifying the frame of the machine so that resonance does not occur. The second-harmonic component of electromagnetic driving force is inherent in the machine and cannot be eliminated; but it may be reduced with respect to the frame by the use of a stiffer stator core, or by the use of a special stator mounting which will not transmit the core response to the frame in cases where such refinements are justified. Whenever excessive vibration is encountered in electric machinery, a compre-

Figure 5. Free vibration of machine rotating assembly produced by a sudden blow from a rubber hammer

Assembly was mounted on rigid supports at the bearings

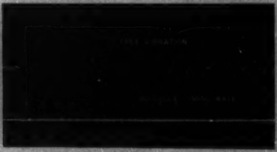

Figure 6. Free vibration of machine frame produced by a sudden blow from a rubber hammer

Frame was supported by three rigid point supports, one at each corner of one end of the frame, and one at the center of the opposite end of the frame

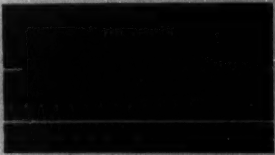

Figure 7. Free vibration of machine frame produced by a sudden blow from a rubber hammer

Frame was supported by three rigid point supports, two near one end of the frame, and the third near the opposite end of the frame hensive study based upon a consideration of the frequency of the components of vibration present and the agencies which give rise to them will point the way to proper corrective measures and modifications which will be effective in reducing the resultant vibration.

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Electrothermal Space Heating and Air Conditioning

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SPACE heating of dwellings has been one of the prime necessities in the improved living conditions of the human race and one that has received considerable study from the earliest time. As the various heating methods were developed, from the early open fireplace to modern heating equipment, utility and esthetic designs for the heating equipment were given a great deal of consideration.

In Sweden very artistic heating units were developed approximately 150 years ago of ceramic glazed tile, which were extremely attractive in design as well as being useful. These devices used small quantities of fuel to heat the entire mass of tile, and the heat energy stored in this tile was radiated into the room, furnishing the required heat for the day.

In the last 20 years in the Pacific Northwest, experiments have been conducted continually, and many practical installations which utilize electric energy for heating have been made. However, these installations have been almost entirely of the radiant and convection type of heater. This type of heater requires an uninterrupted supply of electric energy. In 1935 at the city of Grand Coulee, adjacent to Grand Coulee Dam, an electric heat-storage installation was constructed and placed in operation utilizing granite boulders for the heat-storage medium. This installation was operated for approximately two years on an experimental basis, the results of which experiment were outlined fully in a report by the State College of Washington experiment station.[1] Also in the Pacific Northwest, several installations of storage heaters utilizing hot water are in operation with varying degrees of success.

The problem of filling valleys and reducing peaks in daily load curves of utility systems has existed since the early days of electric distribution. Daily load factor has a direct bearing on cost of service. Increasing load factor decreases cost of service. Incentive rates have been established by utilities in order to encourage use of electric service during off-peak hours, as witnessed by special rates established for hot-water heating and the development of devices for controlling use of electricity during predetermined hours. The present-day prices charged for electric energy used by consumers in the Pacific Northwest are low enough to have encouraged serious consideration of the adoption of electric space heating.

The increased cost of oil, coal, and wood fuels, and the severe curtailment of oil is an incentive for the use of electric energy in space heating.

These conditions led the author to consider the development of a method for utilizing an electric-energy–heat-storage system which would permit the use of off-peak electric energy obtained principally during the night hours

In designing the electrothermal storage system for space heating, consideration was given not only to the heating but also to the problem of air conditioning in as simple a manner as possible, bearing in mind that the electrical method was the one offering the greatest possibilities for regions having low-cost hydroelectric energy as available in the Pacific Northwest. Cleanliness was also a prime factor to be considered. It was decided that a medium for heat storage should be selected which offered extreme flexibility and provided for the storage of a large amount of heat energy in the minimum of space. Water under moderate pressures and temperatures seems to offer the best solution to this problem. Therefore, a system was devised wherein water is stored in an unfired pressure vessel under a pressure of 200 pounds per square inch and at the saturated temperature corresponding to this pressure. Electrothermal space heating is based on fundamental thermodynamic principles, when a common fluid (water) is used for the storage of heat generated by electric energy. The heat is stored in the fluid under pressure. As the pressure is reduced, the sensible heat of the fluid is converted into latent heat of vaporization. The vapor carries this heat to the heat exchanger and liberates the latent heat when condensation takes place.

By the use of this method of off-peak heat–energy storage, economical use of a utility's distribution system can be accomplished. The present distribution systems of the various utilities are being

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utilized at a relatively low load factor. Greater use of these facilities during the periods when the load factor is low permits the fullest possible use of the system with only minor expansion of the distribution facilities. Any added use of these facilities is a source of additional revenue to the utility with only slight additional cost. Another very important factor of the off-peak storage system is that it does not require the utility to guarantee uninterrupted service. This factor has caused the utilities in general to back away from space-heating loads, because additional equipment is required on their part to render this uninterrupted service. Normal outages in no way would affect the heating of the dwelling, if the storage system is in use. As a general thing, the rate structure of a utility is set up to carry all fixed charges and operating expenses plus a reasonable profit on the distribution of energy at a moderate load factor. Therefore, the additional energy distributed over their existing facilities without much additional cost to them is very desirable. The revenues derived from this additional energy will result in a greater net income, which in turn can be shared with the users of electric energy, thus benefiting both the utility and the user.

With reference to Figure 4, a typical daily load curve of the Pacific Power and Light Company (main system) for December 16, 1943, shows the night load between midnight and 6:30 a.m. to be less than half of the peak load during the evening hours from 5:00 p.m. to 7:00 p.m. On this load curve the morning peak between 8:00 a.m. and 12:00 noon is slightly lower than the two-hour peak between 5:00 p.m. and 7:00 p.m. However, some of the systems reverse this order and have a higher peak during the morning hours, but the duration of both morning and evening peaks are approximately as shown on this load curve. It is the object of the off-peak electrothermal heating system to use as much as possible of the available energy represented by the valleys in this curve for space-heating purposes. The shaded section above the load curve represents the energy that would be used by 100 homes similar to the one that was chosen as a demonstration unit at the Midway substation of the Bonneville Power Administration, which is located in the area of central Washington served by the Pacific Power and Light Company. It is estimated that the off-peak energy represented by the valleys of this load curve could be used to heat 1,500 five- or six-room dwellings.

The electrothermal off-peak heat-storage system was designed to meet all of the safety and insurance requirements of the state of Washington, the city of Seattle, and the city of Tacoma, as well as the code of the American Society of Mechanical Engineers for unfired pressure vessels. All electric equipment used in this system has been approved by the national Underwriters' Laboratories.

Principal Equipment of the Electrothermal Off-Peak Storage System

A general description of the electrothermal storage-space-heating and air-conditioning equipment consists of the following:

> The main items of equipment are a superheated hot-water accumulator, immersion electric heaters providing the source of heat, a reducing valve for automatically releasing the heat energy from the accumulator to a lower pressure level, a heat-transfer element for transmitting the heat energy into the air used for heating the dwelling, a condensate receiver to store the condensate until such a time as it is returned to the accumulator, an electrically controlled feed pump, a thermostat for maintaining a constant predetermined temperature in the dwelling, a humidistat for maintaining the relative humidity at a predetermined level, renewable dust filters, activated carbon filters, and an air-circulating fan.

Method of Operation of Electrothermal Off-Peak Storage System for Winter Heating

A schematic diagram of the electrothermal off-peak accumulator system is shown in Figure 1. A description of the method of operation of this system is outlined here.

The accumulator *A* is an unfired pressure vessel filled 90 per cent full of water under normal operating conditions. When fully charged the pressure in the accumulator is maintained at 200 pounds per square inch and 388 degrees Fahrenheit. The accumulator is built according to the specifications of the latest unfired

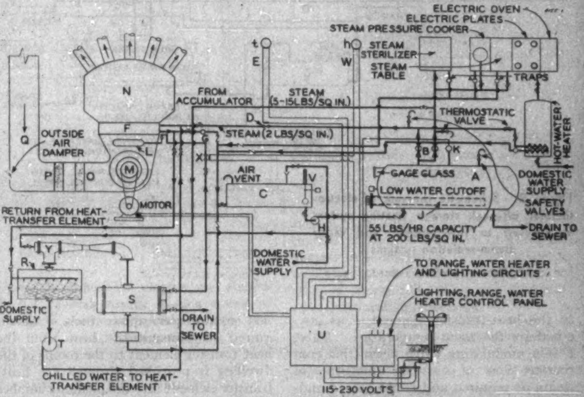

Figure 1. **Schematic diagram of complete electrothermal off-peak accumulator heating and air-conditioning system**

Equipment:

A. Electrically heated water tank and accumulator. Normal operating condition 90 per cent full
B. Steam-pressure reducing valve
C. Condensate receiver
D. Modulating thermostat control valve
E. Room thermostat and time clock
F. Heat-transfer element
G. Condensate return trap
H. Feed-pump
J. Immersion electric heaters
K. Pressure control
L. Humidifier
M. Air fan
N. Plenum air chamber
O. Activated carbon filter for air purification
P. Removable air filter for dust
Q. Return air duct from rooms
R. Evaporator
S. Condenser
T. Chilled-water pump
U. Control panel
V. Float switch in condensate receiver
W. Room humidistat
X. Humidity-control valve
Y. Thermocompresser unit

Specifications:

All electrical controls to be Underwriters' approved standard. Accumulator to be approved ASME unfired pressure vessel for 200 pounds per square inch and 388 degrees Fahrenheit. Valves, fittings, and piping to be American standard for 200 pounds per square inch working steam pressure

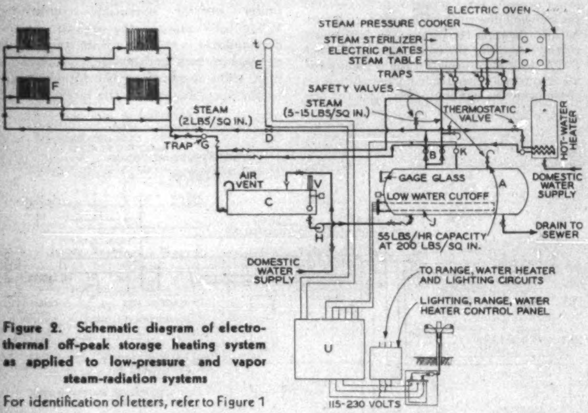

Figure 2. Schematic diagram of electrothermal off-peak storage heating system as applied to low-pressure and vapor steam-radiation systems

For identification of letters, refer to Figure 1

pressure vessel Code as published by the ASME, which insures a pressure vessel having a high factor of safety for use in dwellings. The vapor above the water in the accumulator is released through a pressure-reducing valve *B*, which reduces the pressure from 200 to 2 pounds per square inch. The two-pound per square inch steam line is provided with a safety valve in order to limit the pressure in this part of the system to not more than five pounds per square inch. Thermostatic control valve *D*, which is under control of the room thermostat and night setback time clock *E*, regulates the flow of steam to the heat-transfer element *F* as governed by the room temperature. Valve *D* is a modulating valve permitting continuous flow of varying quantities of steam as required and is not the on-and-off type. As the steam is condensed in the heat-transfer element, the flow of condensate is controlled by a thermostatic trap *G*, which allows only the liquid to pass. The condensate is returned to the condensate receiver *C*, where it is stored until such a time as the liquid level has risen to a predetermined point. At this predetermined point the float switch *V* energizes the feed-pump circuit and permits the feed pump *H* to return the condensate to the accumulator *A* at the slow rate of approximately 0.25 gallon per minute. The condensate cannot be returned to the accumulator unless the heating elements *J* are energized. This low rate of feed to the accumulator was fixed, so that this water could absorb nearly all of the required heat energy from the immersion electric heaters, rather than deriving it from the high-temperature water in the accumulator. It is evident that with this closed circuit there will be little or no loss of water which would require an appreciable amount of make-up water. Because of the closed circuit there is no accumulation of solids from the water to form objectionable scale in the system. Safety features in connection with the hot-water accumulator are a safety valve with outside discharge, which has a steam-flow capacity considerably in excess of the heat-generating capability of the electric immersion heaters *J*, and an electric pressure control *K* which interrupts the power circuit to the electric heaters when the pressure in the accumulator has reached the predetermined pressure of 200 pounds per square inch. Air required to transmit the heat from the heat-transfer element to the rooms of the dwelling is propelled through the heat-transfer element into the plenum chamber *N* by the forced-air-circulating fan *M*. From this point air is conducted to the rooms of the dwelling by well-designed and well-constructed air ducts. The return air from the rooms is conducted through the return air ducts *Q*, and is passed through removable dust filters *P*, which remove all dust and lint from the air. This air then passes through activated carbon filters *O*, which remove all household odors. Outside air is admitted to the suction side of the fan in sufficient quantities only to maintain the oxygen and the carbon-dioxide contents of the air at normal levels. By recirculating the air in the system, with only this small addition of outside air, considerable heat is saved, and a slight positive pressure inside is maintained, thus reducing infiltration of outside air. To maintain the proper relative humidity in the dwelling, a humidifier *L* is provided. The moisture for humidification is provided by a fine spray nozzle that injects the water into the air stream in the form of a mist. This mist then passes through the heat exchanger and is completely vaporized and mixed with the heated air. The flow of water to the spray nozzle is through valve *X*, which is under the control of room humidistat *W*. To govern the flow of electric energy to the system, time switches are provided to determine the point at which the energy is cut off during the peak period of the day and then re-established during the night hours. A second time switch is provided which will permit a limited amount of energy to be taken during the off-peak period between 12:00 noon and 5:00 p.m. The controls are operated so that the various heaters are energized in groups; therefore, the entire heater load cannot be thrown on the system at one time. This function is controlled by a motor-driven step controller which permits the energization of the first four kilowatts of heater capacity, then the next six kilowatts of heater capacity, and finally the last four kilowatts. Should the demand for heat during the day vary from the maximum, the step controller will reverse its direction of rotation and de-energize the heaters in steps in order that the predetermined pressure of 200 pounds per square inch in the accumulator will not be exceeded.

Method of Operation of Electrothermal Off-Peak Storage System for Summer Cooling

For summer cooling, the steam supply to and condensate supply from the heat-transfer element are shut off by the closing of valves in those lines, and the two valves controlling the chilled water to and from the heat-transfer element are open, and a summer switch governing the automatic controls is placed in the summer position. This automatically energizes the summer cooling equipment and controls. The cooling is accomplished by circulating chilled water through the heat-transfer element *F*. This chilled water is produced by the use of a small vacuum water-refrigerating unit which comprises a thermocompressor unit, condenser, and evaporator. The steam from the accumulator is utilized to operate the thermocompressor *Y*, which together with secondary steam-jet ejectors produces a vacuum in evaporator *R*. Return water from the heat-transfer element is sprayed into the evaporator, which operates at a high vacuum causing the evaporation of a portion of the return water. This evaporation removes the heat from the balance of the return water, reducing its temperature to approximately 45 degrees Fahrenheit. The water vapor evaporated, plus the steam supplied to the thermocompressor, is carried over to the condenser *S*, which is cooled by circulating water from the domestic water supply. The condensate from the condenser is returned through a vacuum trap to the condensate receiver *C*, and thus reenters the closed circuit to the accumulator. The chilled water from the bottom of the evaporator is circulated by a chilled water pump *T*. This pump is controlled by the room thermostat *E*, thus maintaining a temperature within the dwelling approximately 15 degrees Fahrenheit below the outside air temperature. As the entire system is controlled automatically, after being adjusted, it requires little attention for operation.

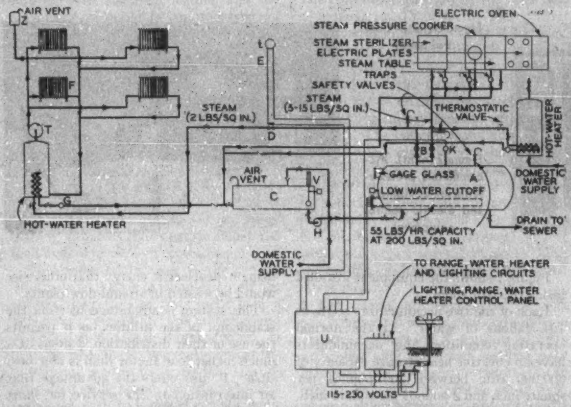

Figure 3. Schematic diagram of electrothermal off-peak storage heating system as applied to gravity and forced-circulation hot-water heating systems

Equipment:

A. Electrically heated water tank and accumulator. Normal operating condition 90 per cent full

B. Steam-pressure reducing valve

C. Condensate receiver

D. Modulating thermostat control valve

E. Room thermostat and time clock

F. Hot-water radiation

G. Condensate return trap

H. Feed pump

J. Immersion electric heaters

K. Pressure control

T. Hot-water pump, either forced or gravity circulation

U. Control panel

V. Float switch in condensate receiver

Z. Expansion tank

With an accumulator system of this kind in a dwelling, advantage can be taken of the stored heat energy to provide sterilizing steam for use in the kitchen, as well as to provide a source of steam-heat energy for pressure cooking of food and for a steam table to maintain prepared food at a uniform temperature, thus making available to the occupant of a small dwelling all of the conveniences that now are available in the most modern hotel and restaurant kitchens.

Low-Pressure Steam Installations

Figure 2 shows adaptability of the electrothermal heating system to existing dwellings having low-pressure steam-heating installations.

The electrothermal heating unit can be used as the source of heat energy for existing dwellings that have installations of low-pressure steam radiators *F* for heating. It is not considered necessary to repeat the description of the various elements in the electrothermal heating system, as these have been described earlier in this paper. In order to adapt the electrothermal storage heating system to a low-pressure steam-radiator installation, it is necessary only to connect the two pounds per square inch steam-discharge header from the hot-water accumulator to the main steam riser of the existing system. The main thermostat *E* will respond to the room temperature and regulate the steam flow through the modulating-thermostat-control valve *D*. By this method of control the steam flow to the radiators varies with the demand for heat as indicated by the thermostat. The condensate from the radiators is returned through the common drain header back to the condensate receiver *C* through the thermostatic trap *G*. Here again the system is a closed one, and the condensate is continually re-used with only slight make-up water being required. With this low-pressure steam-heating system, no control of relative humidity or air conditioning is possible unless additional ventilating ducts and equipment are installed. The various uses to be made of the steam in the kitchen and for hot-water heating remain as previously described.

Hot-Water Heating

Figure 3 shows the two general types of hot-water-heating systems that are installed in existing dwellings, namely, gravity systems and forced-circulation systems. The use of the electrothermal off-peak accumulator system with either type of the previously mentioned installations is practically as simple as that used in the low-pressure steam radiator system. In order to adapt the electrothermal accumulator system to hot-water heating, it is necessary to provide only a small hot-water tank fitted with an immersion steam coil. This coil functions as a heat-transfer element to heat the water of the radiator system. The flow of steam to this immersion coil is controlled by the modulating thermostatic control valve *D*, which in turn is controlled by room thermostat *E*. The circulation in the gravity hot-water system is comparatively slow, which makes it difficult to maintain a uniform room temperature. If the hot-water system is of the forced-circulation type, a more even distribution of the water is obtained throughout the system, and the water temperature can be governed more uniformly by the thermostat. The hot-water

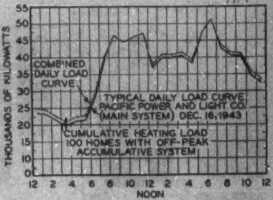

Figure 4. Typical daily load curve of a utility system

Effect of space-heating load on Pacific Power and Light Company. 100 homes—accumulator system

Figure 5. Chief operator's cottage at Midway substation

heating systems do not provide humidification nor air conditioning. In order to obtain these features it would be necessary to provide humidifying and ventilating equipment.

Midway Cottage Installation

An electrothermal off-peak heat-storage and air-conditioning system has been installed recently in a five-room cottage at the Midway substation of the Bonneville Power Administration in central Washington (see Figure 5).

The cottage has no basement. Therefore, to conserve space in the dwelling, the entire electrothermal and air-conditioning unit was installed in the attic.

The cottage is well-constructed with insulated floors, side walls, and ceilings. All windows are tightly weather-stripped, and both front and back entrances are vestibuled with double doors. The design conditions at Midway are minus five degrees Fahrenheit outside and 70 degrees Fahrenheit inside, with a computed heat loss for the dwelling of 35,000 Btu per hour.

All of the heating equipment is housed in a small well-insulated room constructed in the attic. This room is easily reached through a hatch from the utility room on the first floor.

The electric control board was placed on the outside wall of the small equipment room to make it accessible for testing purposes. This board is shown in Figure 6.

The cold air return duct system was installed under the floor, and the hot air ducts were placed over the ceiling joist. The hot air is distributed from the duct system through registers or grills placed near the ceiling line, affording a very even method of distribution of the air in the dwelling.

Two electrothermal hot-water accumulators were used in this installation instead of a single larger one in order to secure the required heat-storage capacity and yet be small enough to pass through a 24-inch by 24-inch hatch to the attic. The arrangement of the equipment in the room is compact yet accessible for maintenance purposes.

Hot-water accumulators are insulated with $4^1/_2$ inches of magnesia block and 4 inches of rock wool and then covered with duck which is pasted on.

The entire duct system is insulated with rock wool, which is wired on and finished by covering with duck.

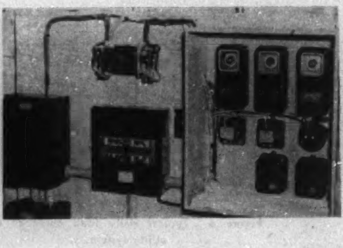

Figure 6 (left). Control panel

Figure 7 (right). Accumulators, pressure-reducing equipment, and electric control equipment

Figure 7 shows the equipment installed inside the attic room.

Each of the two accumulators contains 104 gallons of water. At the normal operating condition the accumulators have an effective heat-storage capacity of 300,000 Btu, between 200 pounds per square inch and 2 pounds per square inch.

The Midway installation incorporates all of the features discussed in connection with schematic diagram, Figure 1, with the exception of the refrigerating unit. This will be installed as soon as it is completed. Weather conditions have been too warm to obtain any scientific data on this heating system.

Conclusions

Electrothermal space heating and air conditioning is based on thermodynamic principles and is very simple. The equipment involved in the construction of the system is standard and can be supplied by existing manufacturers.

Heating and heat-storage features can replace oil, coal, or wood-burning equipment used in existing heating systems in dwellings, such as hot-air heating, Figure 1; low-pressure and vapor-steam heating, Figure 2; and gravity or forced-circulation hot-water systems, Figure 3. It is also possible to make use of the stored heat energy for sterilizers, pressure cooking, and steam tables, giving the housewife all the advantages of the modern hotel kitchen.

The system is practical and economical to install in any dwelling or building now existing or in new construction. It is estimated that the cost for converting a hot-air heating system to the electrothermal storage heating system in a six-room dwelling would not exceed $600.

The electrothermal heat-storage unit uses daily off-peak electric energy, thus using hydroelectric energy that otherwise would be wasted in stream-flow plants.

This system is advantageous from the standpoint of the utilities, as it permits the use of their distribution systems at a much higher load factor than is now possible. It also offers the advantage that an interruption to the service for short periods of time, which are unavoidable, has no effect on the operation of the storage system.

The consumer derives advantages from the storage system by being assured of a continuous heat supply. Then there are advantages of cleanliness, elimination of all fire hazards, lower insurance rates, simplicity of operation, and minimum space requirements.

Studies indicate that the cost of the equipment installed in the dwelling is moderate and competitive with other first-class heating and air-conditioning equipment on the market at the present time.

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An Investigation of the Relationship Between Temperature and Movement of Cables in Ducts

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MANY complex thermal and mechanical influences govern the relationship between electrical loading and the movement of cable in an underground conduit line. It is probable therefore that a knowledge of the relationship will come only through the analysis of many careful observations of the movement of cable in conduits of different types and in different locations and also under the influence of electrical loadings of various magnitudes and durations. It is apparent that engineers now appreciate the importance of a knowledge of the factors that influence cable movement as well as of the control of these factors as a requisite for higher loading of cables that operate under the load factors normally encountered.

This paper contributes to the general information that is accumulating on the subject the results of tests made over a five-month period in the underground distribution system in Buffalo, N. Y. It also describes the testing methods and the equipment used in the hope that this information will prove helpful to others who may wish to conduct similar tests. Finally the paper advances a theoretical explanation of the relationship between the electrical loading and the mechanical movement of cable. The test results support the theory, but more data are required for a confirmation. It is hoped that the presentation of this theory will stimulate the assembly of additional data.

Description of Conduits and Cables on Test

About 8,600 feet of 350,000-circular-mil 25-kv cable, located in a typical underground conduit line, was operated on a controlled-current loading schedule which permitted the recording of the expansion and the correlated sheath temperature rise over a period of about five months. The load cycle for most of the tests was equivalent to a load factor of less than 30 per cent, although some data were obtained for load factors approaching 100 per cent.

The site of the test was a 4,300-foot conduit run in an area considered typical of underground-system conditions in Buffalo. The conduit installation is less than 15 years old. It contains 16 four-inch fiber ducts installed in a concrete envelope with the individual ducts on seven-inch centers and arranged four ducts wide and four ducts high. Most of the manholes are ten feet long, six feet wide, and about seven feet deep. The entire line is located beneath asphalt street paving.

The general arrangement of the test conduit line is shown in plan and profile in Figure 1. There are 17 sections between manholes, and individual sections vary from less than 100 feet to over 400 feet in length. Some of the sections are in rock, whereas other sections are above the rock line. Some of the sections are submerged in ground water, some are what might be termed moist, and a few are relatively dry.

The conduit is not subjected to external heat influences and carries only three three-conductor 350,000-circular-mil, 25-kv cables all located in the bottom row of ducts. Two of these cables, in adjacent ducts, were used for the tests. The remaining cable carried no load while the test was in progress. The cables manufactured in 1930 have regular sector conductors which are insulated with $^{20}/_{64}$ inch of impregnated paper and shielded with three-mil-thick copper tape over the insulation. The insulated conductors are bound together with a five-mil-thick steel binder. The lead sheaths are $^{9}/_{64}$ inch thick. The complete cable has an over-all diameter of $2^{7}/_{8}$ inches and weighs about 12 pounds per foot.

These cables, installed in their present location in 1931, have attained sheath surface dullness and rest in the ducts in a normal fashion. They have never been loaded above a small fraction of their normal carrying capacity. They are arranged rather well in the manholes with the splices centrally located on hangers approximately 40 inches apart. The splicing sleeves are 28 inches long and $5^{1}/_{2}$ inches in diameter. The two cables included in the test are carried through the manholes on the same 17-inch arms. Radii of the bends in the manholes vary considerably but fall in the general classification of 15 inches at the duct mouth and 30 inches at the support arm with a total offset from the line of ducts of 18 inches.

Description of Test Equipment

The two test cables, located in adjacent ducts, were connected in series, and the three conductors were connected solidly together at one end of this test circuit. The power supply to the test circuit was provided through a three-phase low-voltage source with variable-voltage control, capable of providing up to 380 amperes per phase to the test cables. This maximum current produced roughly 18 watts loss per foot of cable.

Time-clock control was provided so that the test current was initiated and interrupted on a selected time schedule. The test-current magnitude, however, was manually preselected with the result that unit cable losses varied somewhat as the test-cable conductors heated up on each load cycle.

Cable-sheath temperature was measured by a special probe which fits into the same duct occupied by the cable. The probe is fitted with an "advance-copper" thermocouple which is carefully shaped to make a large-area contact when it is pressed against the cable sheath by the action of a bellows. The couple is mounted on a small bakelite plate which is attached rigidly to the formed end of a small-diameter steel pipe. A small metallic bellows is attached to the pipe probe on the side opposite the couple. Figure 2 shows the couple end of the probe.

The probe is inserted, couple end first, into the duct containing the cable to be checked. When the couple is in the duct six or seven feet from the manhole duct entrance, and contact between sheath and couple is evident from an electrical-continuity test between couple and cable sheath, a pressure of 15 pounds per square inch is applied to the bellows by way of the pipe probe. The bellows presses against the duct wall and forces the couple against and slightly into the cable sheath.

Sheath temperature is read by using the thermocouple in combination with a potentiometer which is compensated for ambient temperature. During the tests the temperature measurements were taken at intervals of roughly 30 minutes and always at the close of each test. Unattended recording equipment was not used because of the possibility of losing thermal contact with the cable as a result of cable expansion. However, the probe was left in the occupied duct between readings to reduce effect of thermal lag.

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Table I. Summary of Temperature

Curve	Date of Measurements (1943) From	Date of Measurements (1943) To	Current in Amperes	Duration of Current in Hours	Manhole 145 (East) Sheath Temp—C	Manhole 145 (East) Sheath Temp Rise—C	Manhole 145 (East) Expansion in Mm	Manhole 171 West Sink Temp—C	Manhole 171 West Max Sheath Temp—C	Manhole 171 West Sheath Temp Rise—C	Manhole 171 West Expansion in Mm	Manhole 171 East Sink Temp—C	Manhole 171 East Sheath Temp—C	Manhole 171 East Sheath Temp Rise—C	Manhole 171 East Expansion in Mm	Manhole 205 West Sheath Temp—C	Manhole 205 West Sheath Temp Rise—C	Manhole 205 West Expansion in Mm	Manhole 205 East Sheath Temp—C*	Manhole 205 East Sheath Temp Rise—C	Manhole 205 East Expansion in Mm
A	5-13	5-20	260	4	23	13	4.5	10	24.5	14.5	4.5	10	21.5	10.8	3.5	25.7	13.5	4.5	20	6.8	2.0
E	6-10	6-10	260	7			6.0				5.5				4.0			6.0			4.0
F	6-14	6-14	260	4			4.5				4.5				3.0			4.0			2.5
G	6-15	6-15	300	4			7.0				7.0				5.0			6.0			4.5
H	6-16	6-16	380	4			15.5				13.0				11.5			15.0			10.0
I	7-26	7-27	260	4			4.5				4.5				3.5			4.5	32.0	6.5	2.5
J	8-30	8-30	260	4																	
K	8-31	8-31	300	4			7.2				6.8				6.5			6.0			5.5
L	9- 1	9- 1	380	4			15.0				14.5				12.0			15.0			11.0
M	9- 7	9- 7	260	4			5.0				4.5				3.5			5.0			3.0
N	9- 8	9- 8	300	4			8.0				7.0				7.0			7.5			5.5
O	9- 9	9- 9	380	4			15.0				14.5				12.0			14.0			12.0
B	9-14	9-23	260	4	30	12	5.0	18.5	33	14	5.0	18.5	29.5	10.5	4.0	35.3	13.8	5.0	28.0	7.5	4.0
C	9-28	10- 1	325	72	48	29	16.0	17.5	51	32	16.5	17.5	44	25	15.0	51.0	30	14.5	38.2	18.7	12.0
D	10- 8	10-14	300	149	44	26	15.0	16.5	48.5	30	14.0	16.5	42	24	14.0	48.5	29	12.0	38.0	19.0	10.0

* Four to eight-watt-per-foot heaters in adjacent ducts in section from manhole 317 east to manhole 347 west from 9-7-43 to end of tests. Heaters on same time cycle as test cables.

A continuous record was made of the expansion and contraction of the test cables at the duct entrances in the manholes. This was accomplished through the installation of graphic recorders made up from old graphic demand instruments. A flexible shaft, attached to the cable at the duct edge, transmits cable motion to the recorder mechanism and pen and provides a continuous record of cable motion on paper strip charts. This arrangement of the recorder is shown in Figure 3.

Mercury-bulb thermometers were used to obtain sink and empty-duct temperatures. A small-diameter steel pipe about four feet long was driven through the manhole wall about four feet below grade and at right angles to the line of the conduit run to accommodate the thermometer for sink measurements. The locations at which sink temperatures were measured are shown in Figure 1.

Test Schedule

Before starting the tests, each manhole was carefully inspected, all subnormal arrangements of the cables were corrected, and all fireproofing was removed from the cables. The field survey was started by taking photographs of the interior of all of the manholes by means of special photographic equipment which fitted into the manhole-cover openings in such a way that all pictures taken of each manhole were from the same camera position and angle of exposure. This made possible a direct comparison of photographs to determine the changes in the positions of the cables, splices, supports, and so forth, and for correlation with field measurements taken during the tests. The advantage of this care in the photographic work is shown in Figure 4, where a picture taken during a severe load cycle is superimposed on one taken after the testing was completed. Figure 5 shows the photographic equipment itself.

Bending radii and location dimensions were taken to establish the position of the cables at the start of the tests. Current then was applied in two four-hour 260-ampere cycles each day. This loading, four hours on and eight hours off in each 12-hour period, was about double the 24-hour load factor for which ratings were desired, but at the low heating level no carry-over of heat between the individual cycles occurred. This loading developed about seven watts per circuit foot of cable.

Temperature observations during the first few load cycles showed appreciable increases in only six sections. Expansion recorders were installed on the 12 ends of these six sections. The balance of the conduit line was either quite moist or partly flooded.

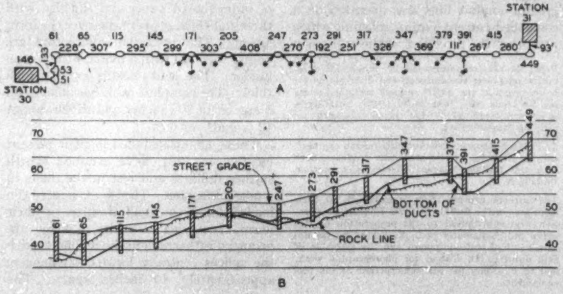
With a few interruptions for spot checks, the test schedule of two 260-ampere four-hour cycles every day was continued from May 13 to August 11. From that date to September 26 the schedule was changed to one four-hour 260-ampere load cycle per 24-hour period. During this latter schedule special heating cables, each having losses of eight watts per foot, were installed in four ducts adjacent to the test cables in one of the sections which showed a large temperature rise during the earlier tests. The purpose was to simulate the effect of additional loaded cables in this section of conduit by the addition of losses equal to 32 watts per foot. The heating cables were energized and de-energized on the same schedule as the cables under test.

A few special tests were made at higher values of loading and some for longer durations in order to obtain data on the thermal characteristics of the conduit system. These data subsequently proved

Figure 1. Plan and profile of the test conduit line

* indicates location of sheath-temperature thermocouple and cable-expansion recorder

# indicates location of earth temperature thermometers



and Expansion Measurements

Manhole 247						Manhole 273 (West)				Manhole 317 (East*)			Manhole 347								Manhole 379 (West)			
West			East										West*				East							
Sheath Temp—C	Sheath Temp Rise—C	Expansion in Mm	Sheath Temp—C	Sheath Temp Rise—C	Expansion in Mm	Sink Temp—C	Sheath Temp—C	Sheath Temp Rise—C	Expansion in Mm	Sheath Temp—C	Sheath Temp Rise—C	Expansion in Mm	Sink Temp—C*	Sheath Temp—C	Sheath Temp Rise—C	Expansion in Mmm	Sink Temp—C	Sheath Temp—C	Sheath Temp Rise—C	Expansion in Mm	Sheath Temp—C	Sheath Temp Rise—C	Expansion in Mm	Curve
20	9.5	2.5	19.2	7.3	3.0	11.5	21.5	9.0	3.0	24.3	11.5	4.5	11.2	26	12.5	4.5	11.8	19.8	8.5	2.5		10.8	3.0	*A*
		3.5			3.5				3.5	27.0	12.2	5.0	17	29.5	13.5	5.0	17	25.5	10.5	3.0			5.5	*E*
		3.0			3.0				3.0	26.7	10.2	4.0	17.5	29.5	11.3	5.0	17.5	25.5	8.1	2.5			3.0	*F*
		4.5			4.5				4.5	30.4	12.3	6.5	18	34.5	17	6.5	18	29.0	12.5	4.5			5.5	*G*
		9.5			11.5				8.5	40.5	21.5	14.0	18.5	44.8	24.3	13.0	18.5	35.0	16.5	8.5			8.5	*H*
30.5	8.0	3.0			3.0				2.5	32.8	9.6	4.5	25	36.5	10.8	4.5	25	33.7	7.5	2.5			3.0	*I*
										30.5	9.5		24	33.7	9.2		24	30.5	6.0					*J*
		5.0			6.0				5.0	34.5	13.5	6.0	23.5	38.1	15.4	6.0	23.5	33.7	11.0	4.5			5.0	*K*
		9.0			12.0				9.0	42.5	21.5	15.0	23.8	48.0	24.8	13.0	23.8	41.7	18.5	10.0			9.0	*L*
		3.0			3.0				4.0	32.3	11.6	6.0	24	34.7	12.5	5.5	24	30.5	8.3	3.0			3.5	*M*
		5.0			7.0				5.0	36.7	15.7	9.0	24	40.0	17.2	7.5	24	34.2	11.4	5.0			5.5	*N*
		10.0			12.0				9.0	44.2	22.7	16.0	24	49.7	26.1	14.0	24	41.5	18.3	10.0			9.0	*O*
28.8	7.8	3.5	28.5	8.0	4.5	21.5	29.2	8.2	3.5	32.5	12.5	5.5	21.5	36.8	14.8	5.5	21.5	31.5	9.5	3.5	31	10	3.5	*B*
41.0	20	12.0	40.5	20.5	10.5	19.5	41.2	21.2	9.0	53.2	34.2	23.0	20.3	60.7	39.7	17.5	20.3	44.0	24.0	13.0	44	23	10.0	*C*
38.5	18	10.0	38.0	19	8.0	16.8	41.5	22.0	8.5	52.0	33	25.0	19	61.5	41	16.0	19	42	23.0	11.0	42	22	8.0	*D*

to be very informative in the study of the expansion phenomena.

Test Results

Figure 6 has been prepared as a general picture of the increase and decrease of the linear dimensions of the six cable sections as recorded by the equipment at each end of each section. This information is presented by plotting the recorded positions at the time of maximum longitudinal expansion and maximum contraction for each end of each cable section during each test cycle. These maximum and minimum points are joined together by straight lines except for the two long-duration load cycles and for those cases where the loading was discontinued for appreciable periods. The expansion and contraction curves for these special tests have been plotted on the basis of four points taken from the record during each 24-hour period.

The scale used for longitudinal expansion in Figure 6 indicates cable motion in the manhole. No movement of cable into the duct beyond the starting point was recorded in the six sections where recorders operated.

The gaps in the curves shown in Figure 6 are the result of mechanical difficulties in the operation of the recorders. The data so lost are not of serious consequence.

Table I shows the maximum sheath temperatures measured during each test for the six sections referred to previously. It should be noted that the sheath temperatures given in the tabulation were taken at different loadings, at different ambient temperatures as shown by the sink temperature measurements and also at different load factors. The tabulation gives the expansion of the cable that appeared in the manholes at each end of each section as a result of the measured sheath temperature rise also shown in the tabulation. All of the values for expansion were obtained by scaling the curves of Figure 6.

The variation in the sheath temperature increases at the 12 test points as shown graphically in Figure 7 at four different times and at three different heating levels.

Figure 8 is a graphic report of the total expansion during each test for each of the six sections on which recorders were installed. The data for Figure 8 were obtained by adding together the recorded expansion of the two ends of each of the test sections to obtain the total expansion for each section for each test. The expansion in each case is the increase in length apparent in the manhole for each section and for an individual load cycle, without regard for expansion, permanent set, or other effects which accumulate as a result of such factors as changes in earth temperature and motion of the sections as a whole, which produce long-time changes and irregularities.

The bellows provides the force to insure good thermal contact between the thermocouple and the cable sheath at a point six or seven feet inside the duct

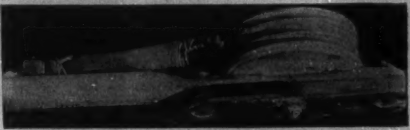


Figure 2. Sheath-temperature-thermocouple probe

Analysis of Tests

The first thing of interest found in analyzing the test results is that the measured temperature rise at the test points is remarkably free from radical variation with time. This is brought out particularly well in the two curves at the bottom of Figure 7 which are plotted from data taken during tests made four months apart.

Another interesting observation from Figure 7 is that the maximum variation in measured temperature rise for the whole group of test points in any test approaches two to one. This is not surprising, but it is important since it could indicate that the variation of temperature over the individual section lengths is considerable and that measurements of temperature taken at the ends of the section may not be a true indication of average section temperature.

However, the comparison of the temperature measurements at the ends of the individual sections for each test shows a range of temperature variation considerably less than that for the whole test run. Furthermore, the relationship between the individual end temperatures is maintained with change in load factor and load level as shown by a comparison of the shape of the upper curves with that of the lower curves in Figure 7.

The plotted temperature rise, Figure 7, has many of the characteristics of the elevation profile, Figure 1, in that the maximum temperature rise of each section is at the end of maximum elevation. This characteristic, in all probability, is due to the tendency of the higher ends of the duct sections to have the minimum moisture in and around the duct bank.

Turning next to Figure 9, one sees that the shape of the curves for total measured section expansion and that of the curves for the average temperature rise have a very close similarity. It is interesting, however, that Figure 9 shows several marked deviations from this similarity in the two groups of curves. At the heating level existing on the 260-ampere four-hour cycle, the measured section expansion in all cases was less than one millimeter per degree centigrade sheath rise, whereas at the higher heating levels found in the 325-ampere 72-hour and 300-ampere 149-hour tests the section expansion was on the average above one millimeter per degree centigrade. In addition, the two lower expansion curves in Figure 9 show that for the 260-ampere four-hour loading there was considerable variation in the expansion as measured at two different times while the temperature rise showed almost perfect repetition.

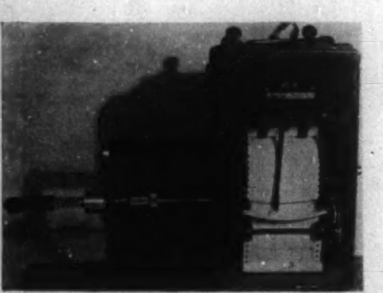


Figure 3. Expansion-recorder mechanism

The flexible shaft transmits cable motion to the recorder pen

The curves in Figure 10 show that the realized elongation of the cable observable in the manholes is less than 50 per cent of the theoretical free expansion of either the lead sheath or the copper conductors.

Fortunately, these three effects, nonlinear relationship of movement with temperature rise, variation with time of year, and failure to approach theoretical elongation, are of interest chiefly in studies aimed at establishing the mechanism of expansion. A given cable installation can be operated satisfactorily with only a general knowledge of the relationship between loading and expansion. This is particularly true, since the arrangement of cable in a manhole is not carried out with scientific precision and with an expectation of permanency. Cable arrangements designed to permit certain top limits of expansion in most cases will deteriorate with time, and only prohibitively expensive maintenance schedules could maintain the design conditions.

In view of these considerations, it is concluded from the temperature-expansion relations shown by the curves of Figure 9 that one millimeter of longitudinal expansion observable in the manholes per degree centigrade sheath temperature rise is a practical design factor. The length of cable section is not an important consideration, because all sections in normal operation are reasonably close to the average length of the six sections on these tests. In addition, the realized longitudinal expansion per unit length of cable is less for a long section than for a short section, and this tends to make the length of the section a rather unimportant factor in determining the anticipated longitudinal expansion.

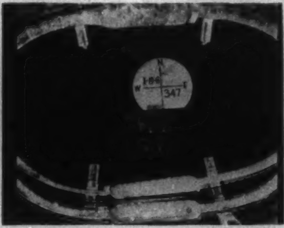


Figure 4. Superimposed pictures to show cable motion in a manhole

The dashed lines outline the positions of the expanded cables under heavy load. The solid lines outline the contracted positions of the cables under no load

On the system under test, two cables were carried by the same 17-inch arms in the manholes. This arrangement will accommodate about 50 millimeters of total section expansion or about 25 millimeters at each duct mouth. The 50-millimeter expansion produces about five inches of lateral movement of the splices. If there is a difference in the loading of the two cables on the same arm, the permissible lateral motion may be reduced.

The 50-millimeter limitation for total longitudinal expansion is acceptable only where the cables are properly arranged in the manholes and where the radii of curvature at the duct mouths and at the arms are appropriate. The life of the cable sheath under the flexing produced by the proposed expansion limit is not known accurately, but our interpretation of the tests reported to the AIEE last year by E. W. Osterreich is that at least 20 years of operation can be expected before sheath trouble due to expansion and contraction becomes serious. Occasional operation at higher temperatures and greater expansions should not effect the life of the sheath but may cause incidental difficulties.

The degree to which the results of these tests will apply to other installations is not known, but it is probable that the reaction of installations involving other sizes and types of cables and ducts will vary considerably from the findings given here. It is apparent, however, from these tests that the longitudinal expansion of the cable as now installed is a more serious limitation than the thermal limitations of the insulation for load cycles such as those experienced in supplying residential load.

Mechanism of Expansion

The tests described in this paper were made to determine the safe limits for the loading of a specific system. However, it is felt that so much improvement can be made in the methods of constructing and installing cable as a result of a more complete understanding of the mechanism of cable expansion under load that we have examined these test results for data

Figure 5. Manhole camera installation

Care in positioning camera in manhole opening permits making a graphic record of cable motion by superimposed pictures as shown in Figure 4

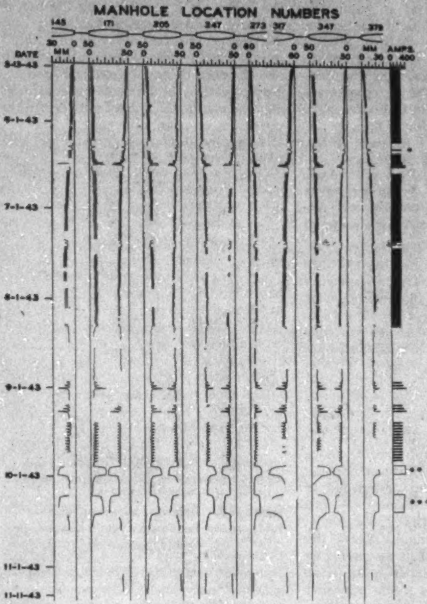


Figure 6. Continuous record of cable expansion

Motion recorded at the 12 ends of the six test sections is plotted against load cycle and date. The scale of motion at the top of the figure is in millimeters. The load cycles are of four-hour duration except those marked *, **, and ***, which are 7, 72, and 149 hours duration, respectively

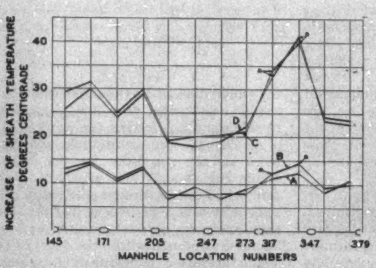


Figure 7. Temperature rise of cable sheath

The heavy, light, and dashed lines connect points plotted for temperatures measured at the ends of the same section, adjacent sections, or sections not adjacent, respectively. For test conditions see Table I

that might be helpful in this understanding. We believe the following observations warrant further consideration:

1. The amount of longitudinal expansion appearing in the manholes for a given increase in cable temperature due to losses in the conductors was different for tests made at different periods of the year.

2. The average longitudinal expansion appearing in the manholes per degree of increase in cable temperature was greater for the larger increases of cable temperature.

3. The amount of longitudinal expansion appearing in the manholes was only about one half of the theoretical free longitudinal expansion of either the copper conductors or the lead sheath (see Figure 10).

The following is a theoretical analysis of cable movement that explains these three observations. The theory is offered with the knowledge that there are insufficient test data to support it but with the hope that it may stimulate further investigation.

Consider first an idle cable in a conduit line which carries no other cables. At the time of lowest earth temperature, the cable is contracted to its minimum length and is in a straight path along the bottom of the duct. As the earth temperature and consequently the cable temperature increases, the cable expands longitudinally. The initial increase in the length of the cable is taken up principally by lateral displacement or "snaking" of the cable within the duct, since the resistance to this "snaking" is less than the resistance to longitudinal motion. Some longitudinal motion is present, and some of the increase in length appears in the manholes, in spite of the freedom of the cable to reposition itself within the duct.

The longitudinal expansion probably is due to the greater thermal expansion of the lead sheath than of the copper conductors. This undoubtedly produces a longitudinal mechanical force in the sheath, part of which is transmitted to the conductors through the insulation, so that it acts to increase the length of the entire assembly. This increase in length of the entire assembly is readily taken up as lateral displacement of the cable within the duct. However, some of the stress in the sheath must be transmitted along the sheath and is effective only as a force to increase the length of the cable into the manholes. The ability of the sheath to produce such movement is limited by the elastic deformation of the sheath and the maximum compressive force which the sheath can transmit longitudinally without permanent deformation.

At the time of maximum earth temperature, which may represent a rise of as much as 25 degrees centigrade above minimum temperature, the cable is expanded sufficiently so that the resistance to added lateral deflection or "snaking" within the duct is considerably increased and the over-all tendency for expansion is into the manholes.

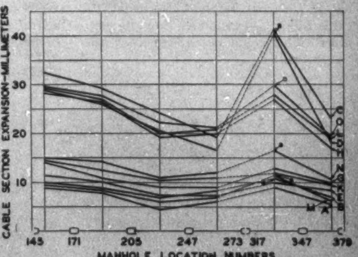


Figure 8. Cable expansion appearing in manholes

The solid and dashed lines connect points plotted for adjacent sections and for sections which are not adjacent, respectively. For test conditions see Table I

Next, assume that the cable under consideration is carrying load. The action of the forces produced by the temperature changes caused by the electrical losses must be analyzed in relation to the effects of ambient temperature which were discussed in previous paragraphs. For example, if a moderate load is placed on the cable at the time of lowest earth temperature, the longitudinal expansion starts with the cable flat in the duct. Under this condition, a large part of the increase in length is readily absorbed by lateral movement within the duct, and, for a given temperature rise, the expansion appearing in the manholes will be a minimum. However, if the same loading is placed on the cable at the time of maximum earth temperature, the increase in cable temperature is the same as before, but the amount of expansion of the cable into the manhole is from 20 to 50 per cent greater. The reason for this, of course, is that most of the lateral freedom of the cable within the duct was absorbed by the expansion due to earth temperature increases.

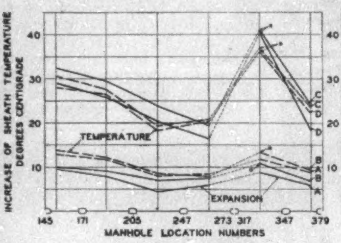


Figure 9. Sheath temperature rise and associated cable expansion appearing in manholes

The solid lines represent expansion. The heavy dashed lines represent temperature rise. The light dashed lines connect points plotted for sections which are not adjacent. For test conditions see Table I

The mechanism of cable movement is complicated by other variables. For example, the temperature at which an unloaded cable will assume a straight line on the bottom of the duct undoubtedly depends on previous loadings. If the cable experiences a relatively severe load cycle so that the space available for lateral displacement within the duct is fully occupied before the top of the cycle, then it would appear reasonable that the cable on cooling would tend to straighten out in the duct before withdrawing the cable which had expanded into the manholes. This would have the effect of raising the temperature level at which the cable would be in a straight line in the duct.

The theory outlined in the preceding paragraphs not only explains the performances observed during the tests reported in this paper, but also accounts for a type of damage that occurs to the lead sheath of cables in operation. If the lead sheath is reasonably free to move with respect to the core of the cable, then the forces of longitudinal expansion of the sheath are in part transmitted into that portion of the sheath in the manhole. These forces are maximum where the movement of the sheath is restrained as at a bend or at a solid-filled splice or at the location where the equal opposing forces of an adjacent section appear. Frequently this results in the lead sheath exceeding its elastic limit at one of these points, and a wrinkle or ripple appears in the sheath all around the cable. Once this ripple develops, all further expansion of the sheath is concentrated at that ripple, and failures do not appear at other locations on the sheath. This type of failure is very commonly found at the ends of splices, particularly on those installations where the cables are installed without bends.

If the lead sheath fits rather tightly on the core of the cable, the longitudinal forces developed between core and sheath due to excess expansion of the sheath are continuously transmitted to the core, and the action of the sheath in straightening the cable is reduced or possibly eliminated.

This suggests the desirability of investigating experimentally the possible advantages of improving the mechanical bond between the core and the sheath of the cable. It is possible that the use of properly shaped core-binder tapes and the use of sheath reinforcement would provide sufficient bond between the sheath and the core to insure greater lateral expansion within the duct and less expansion into the manhole. It may even be within the realm of possibility that a carefully co-ordinated design would make possible the elimination of all expansion loops in a cable installation. If would, of course, be necessary to restrain the movement of such a cable into the manhole, perhaps by a suitable enclosure for the cable within the manhole. If a cable so installed had its sheath and core mechanically interlocked, we believe that the entire expansion would be taken up as lateral deformation within the duct, and, if the operating temperature increases became high enough to cause the sheath stress to exceed the elastic limit, the permanent deformation would be spread over the entire length of cable, and there would not be localized portions of the sheath stressed to the point of failure.

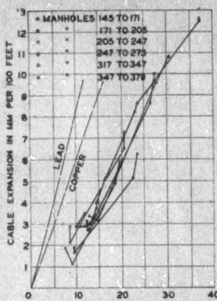


Figure 10. Relation between increase in cable-sheath temperature and realized unit cable expansion

The solid lines connect points relating to individual sections. The dashed lines indicate the friction-free temperature–expansion characteristics of lead and copper assuming a temperature differential between the two metals approximating the differential found in a loaded cable

The Summation of Load Curves

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A NUMBER of load curves may be added to determine some characteristics of a composite load from known values of the individual loads which comprise it. The resulting peak load, minimum load, load factor, or time of the peak are characteristics usually sought, but there may be many others.

Operators seek to maintain high load factor in a power system in given areas, in feeders, and in equipment to obtain maximum use of the facilities, and to this end loads are combined, so far as practical limitations will permit, for optimum results, which are generally measured finally by return on the plant investment. Hence the subject of rates is linked to that of load curves.

For instance, the economic justification for central-station generation as compared to local generation near individual loads, depends primarily on two facts:

1. The unit kilowatt-hour cost of energy delivered has, in general, been lessened as the amount generated has increased.
2. Relative generating capacity, feeder capacity, and substation capacity required have decreased as additional loads have been added and the demand factor has decreased due to the effects of diversity, through the serving of many loads of greatly diverse character, resulting in improved load factor or use of facilities.

The advantage for central-station generation offered by condition 2 is pertinent to the subject matter of this paper. It may be partially neutralized by adding certain types of loads in excessive amounts. The principles involved will be recognized by engineers who are striving to keep their organizations competitive and build up their revenues at the same time.

Also, in wartime, when old economic controls no longer govern, the need for intelligent load forecasting is apparent to all. It is realized now that in the haste to arm and produce, after a prolonged period of comparative business stagnation, there was much overestimation of loads, with corresponding delays because of some demands for equipment not really needed.

Forecasting under war conditions becomes difficult for load characteristics and timing have changed. Factors based upon past observations are no longer reliable. Industries that formerly operated on a single shift may now be on a 24-hour day, seven-day week basis. Wartime has changed the characteristics of lighting loads, while shortages of manpower and materials have had their effect upon commercial loads. Many utility companies have had their peak-load periods displaced as much as 12 hours under war conditions.

It is prudent to examine trends from time to time, and in so doing, one is confronted with the need to determine the components which comprise a load or conversely to determine the result of combining loads. The purpose of this paper is to consider some of the technical aspects of these problems and to present some methods which, it is hoped, will be helpful.

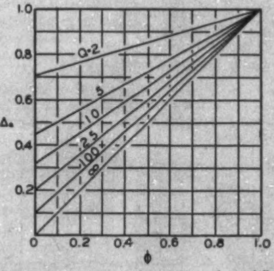

Figure 1. Δ_Q versus ϕ for various values of Q

$\alpha = 2$ and $p = 1.00$

The Addition of Loads Which Are Similar Except for Timing

An understanding of the case in which a number of loads Q, of equal load-factor ϕ, are to be combined, is helpful in determining the methods to be applied to the more general case for loads of various load factors.

In a previous paper[1] load-curve summation was mentioned briefly and the formula presented:

$$\phi_Q = \left[1 + \left(\frac{1}{\phi} - 1\right) Q^{-1/\alpha}\right]^{-1} \qquad (1)$$

in which ϕ_Q was given as the load-factor for the sum of Q equal loads of load-factor ϕ each and α was stated to be a variable of value two or greater.

A more general form of equation 1 is given by:

$$\phi_Q = \left[a + \left(\frac{1}{\phi} - a\right) Q^{-1/\alpha}\right]^{-1} \qquad (1a)$$

where a is the asymptote of the curve of $1/\phi_Q$ versus Q, instead of unity as is the asymptote given in equation 1.

In previous applications of the formula, α was taken as greater than two without introducing a value for a when adjusting for peak concentration. This procedure is not rigorous and does not give true results with any constant α for large range of values of Q and periods of peak concentration because the asymptote is variable and greater than unity when the peaks are concentrated within periods less than that for which ϕ has been calculated.[1]

A preferable approach is to find the value of the asymptote a. With α taken as a constant equal to two, a then becomes equal to z/ϕ, in which z is the coincidence factor for $Q \doteq \infty$ as will be seen subsequently.

Further investigation has convinced the author that for utility services, the number of hours, on the average, during which peaks of a given load factor occur, decreases as the load factor decreases in accordance with probability laws which can be expressed analytically.

Let us examine the meaning of ϕ itself. Suppose a constant load lasts for a period of one hour and no longer. On this one-hour basis the load factor is 1.00; on an eight-hour basis it is 0.125; and on a 24-hour basis only 0.0417; all for the same load. So load factor must always be associated with the time for which it is measured. Also, a time restriction in the occurrence of the peaks alters Δ_Q by changing α or ϕ as given in equation 2, which, for $\alpha = 2$, applies to a random peak-time distribution during the time for which ϕ is considered. If peaks of one-hour duration should all occur during the same hour, they would be added arithmetically. This is one extreme condition. The other extreme condition would occur when the peaks are evenly distributed throughout the time considered.

Since the coincidence factor Δ_Q is the ratio of the peak for Q loads to Q times the peak for one load (the inverse of the diversity factor), we can write, taking the peak for one load as unity:

$$\Delta_Q = \frac{Q\phi}{\phi_Q} \div Q = \frac{\phi}{\phi_Q}$$

so that

$$\Delta_Q = a\phi + (1 - a\phi) Q^{-1/\alpha} \qquad (2)$$

substituting the value of ϕ_Q given in equation 1a. Values of Δ_Q versus ϕ for various numbers of loads are shown on Figure 1 with α taken equal to two and a equal to one. As Q becomes infinitely large $\Delta_Q \doteq \phi$. Also note that as $\phi \doteq 0$, $\Delta_Q \doteq Q^{-1/\alpha}$.

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R. F. Hamilton was with the War Production Board, Washington, D. C., at the time this paper was prepared; he is now with the Foreign Economic Administration, Washington.

Refer to Figure 2. Assume five equal momentary peak loads A, B, C, D, and E, to be concentrated and evenly distributed within a time p. The momentary peak, at the time of the peak C, of the sum of these five loads will be the sum of the five ordinates F, G, C, H, and I, spaced equally in time on the curve FCI. J, K, L, M, and N, are relative instantaneous peak values of the sum of the five loads at the times corresponding to peaks A, B, C, D, and E. Note that L is the highest of these values corresponding to the time of the peak C of the curve which is symmetrical within the time p.

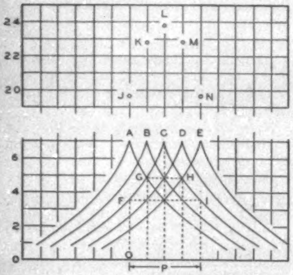

Figure 2. Five equal peak loads occurring within a time p

Now as the number of loads is increased above five, the momentary peak L of their sum will approach as a limit the integral of the curve FCI over the interval of time p. For uniform distribution with reference to time one can therefore write for equal loads:

$$M_Q \doteq Q\cdot p^{-1}\int^{p}\left(\frac{m}{M}\right)\alpha\left(\frac{t}{T}\right)\doteq Qz \tag{3}$$

for $Q \doteq \infty$, measuring time from the point O of Figure 2.

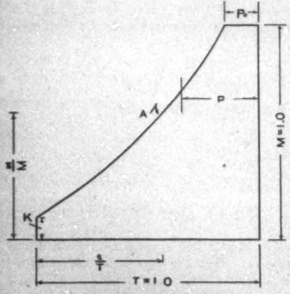

Figure 3. Reversed time-duration curve
M, T, ϕ, k, and p_0 given

This equation simply states that the peak value for $Q \doteq \infty$ loads approaches Q times the average value for one symmetrical load during the interval p. This average value we have called z. For nonuniform distribution during the time p, the peak M_Q, following the most probable random distribution, will exceed Qz by an amount equal to $(1\text{-}z)Q^{1/2}$. This follows from equation 1 using $\alpha=2$ and replacing ϕ with z, or

$$M_Q = Qz+(1-z)Q^{+1/2} \tag{4}$$

$$\Delta_Q = z+(1-z)Q^{-1/2} \tag{5}$$

Also from equation 2 it is apparent that $a = z/\phi$ if α is to be two and equation 5 holds true.

Suppose we now set up a hypothetical load synthetically, taking into consideration minimum value, maximum value, duration of the peak, and load factor, using the methods heretofore described[1], in accordance with Figure 3:

ϕ is the load factor.
k is the variation factor.
p_0 is the peak duration.
T, the total time, is unity.
M, the maximum value, is unity.

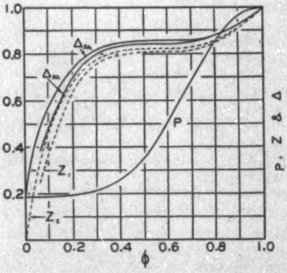

Figure 4. p, z, and Δ_{∞} versus ϕ using equations 8 and 9 for z_1 and z_2

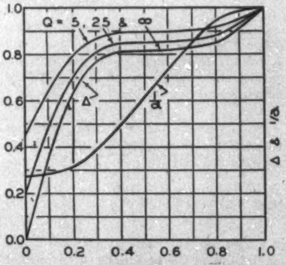

Figure 5. Curves of $1/a$ versus ϕ and Δ_Q versus ϕ for various values of Q

The curve A can be expressed as:

$$m/M = k+(1-k)(1-p_0)^{-n}\cdot\left(\frac{t}{T}\right)^{n}$$

where

$$n=\frac{(1-\phi)}{(\phi-k)-p_0(1-k)} \tag{6}$$

This will represent a load-duration curve with abscissas reversed in sign.

Let p be a period of time which includes the period of the peak p_0, and $(p - p_0)$ in addition. By integrating this load-duration curve over the time p and dividing the result by p, one obtains z, the average value, and hence the load factor for that portion of the load included within the time p.

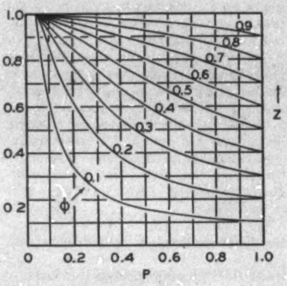

Figure 6. z versus p for various values of ϕ according to equation 8

$$z=k+p^{-1}\left\{(\phi-k)-[(\phi-k)-p_0(1-k)]\times\left[\frac{1-p}{1-p_0}\right]^{v}\right\} \tag{7}$$

where

$$v=\frac{(1-k)(1-p_0)}{(\phi-k)-p_0(1-k)}$$

For a one-hour peak in a 24-hour cycle, $p_0=1/24$; also in utility curves it has been

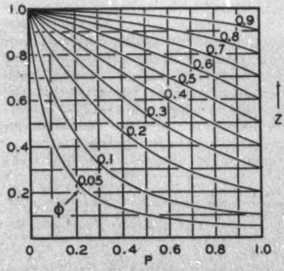

Figure 7. z versus p for various values of ϕ according to equation 9

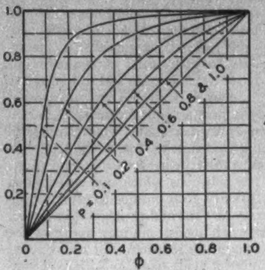

Figure 8. z versus φ for various values of p according to equation 8

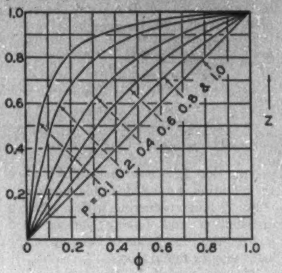

Figure 9. z versus φ for various values of p according to equation 9

found that one can use $k = 0.4\phi$ usually. Under these conditions:

$$z=\frac{1}{10}\left[4\phi+p^{-1}\left\{6\phi-\frac{1}{12}\,[74\phi-5]\times\left[\frac{24}{23}(1-p)\right]^{\frac{23(5-2\phi)}{74\phi-5}}\right\}\right] \quad (8)$$

For momentary peaks or for weekly and monthly load-factors for which p_0 becomes very small and where k approaches zero in value, one can put p_0 and k equal to 0 in equation 6 and then:

$$z=\frac{\phi}{p}\{1-[1-p]^{1/\phi}\} \quad (9)$$

The purpose of equations 7, 8, and 9 will be obvious. If the peaks of a number of loads Q, of equal load factor ϕ, are restricted to a time p, which is less than the time for which ϕ is calculated, then we shall use z instead of ϕ. If our cycle is one of 24 hours, then $24p$ will be the number of hours in which the peaks are concentrated.

Probability Elements as Applied to Consumers' Habits

Consider the not unusual custom of measuring all consumers' maximum demands on a unit-time basis. If these demands in kilowatts are also compared on a unit basis regardless of magnitude then, relatively, a consumer's load factor is a direct measure of his energy consumption throughout the day, and it seems logical to presume that the more energy he consumes the more likely is the time of his peak-load to vary. Stated in another form, when a substantial number of customers with about the same load factor have been segregated, it is found that the time in which the peaks occur can be correlated with the load factor. Such a correlation may be expressed:

$$p=b+ce^{-h^2(f\phi)^2} \quad (10)$$

to conform to Gauss' normal probability curve.

A study of Constantine Bary's results covering large and small light and power, commercial and industrial consumers in Philadelphia, Pa., would indicate values for b, c, h^2 and $(f\phi)^2$ in equation 10 as follows[3] (see figure 4):

$$p=0.187+0.813e^{-4\{(1-\phi)^2+(1-\phi)^{2.6}\}} \quad (10a)$$

Other values of these quantities will be found for other special types of load and may be expected, it is believed, for the same types in other locations.

The curve of $\phi/z=1/a$ versus ϕ also assumes the shape of a probability curve as can be seen in Figure 5, and may be expressed empirically without reference to p and z. However, for momentary peak loads and with p known, a would be given by, (p_0 and $k = 0$):

$$a=\frac{1}{p}\{1-(1-p)^{1/\phi}\} \quad (11)$$

from equation 9 and likewise other values from equations 7 and 8 for the conditions therein given for p_0 and k. The quantity a is the value of the asymptote of equation 1a as previously discussed. Figure 5 also shows the variation of Δ_Q with Q, for the values of $1/a$ on the curve.

The Effect of Peak Time Concentration on the Coincidence Factor

Refer to Figure 4. The curve marked p represents values of p as calculated from equation 10a and $24p$ is the equivalent hours per day in which the total number of peak loads may be considered as concentrated. (See note following example 5 in the Appendix.)

The curve marked z_1 represents values of z calculated from equation 8 using the corresponding values for p just found.

The curve marked z_2 represents values of z calculated from equation 9 using the corresponding values of p. For z_1, p_0 is 1/24 (one-hour peak) and $k=0.4\phi$ and for z_2, p_0 is 0 (instantaneous peak) and $k=0$, as explained previously.

Note that z_1 and z_2 apply to an infinite number of loads. By concentrating the time of the peaks in accordance with curve p, we have shifted the straight diagonal line of Figure 1, $\Delta_Q \doteq \phi$, for an infinite number of cases, to one of the peculiarly shaped curves $\Delta_Q \doteq z$ of Figure 4. See also Figures 8 and 9.

Now by applying equation 5 for the summation of 30 equal loads, we obtain the two curves of Figure 4 shown for Δ_{30}.

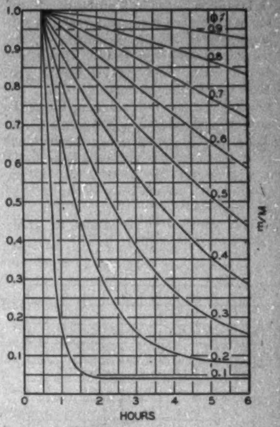

Figure 10. m/M versus hours of peak displacement for various values of φ using equation 12

$k = 0.4\,\phi$ and $p_0 = 1/24$

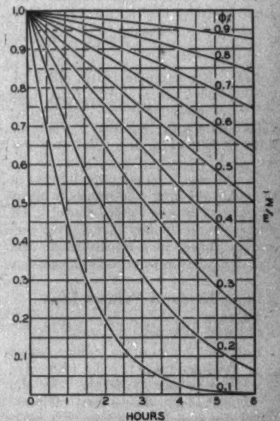

Figure 11. m/M versus hours of peak displacement for various values of φ using equation 12

$k = 0$ and $p_0 = 0$

Tests conducted by Constantine Bary[2], for 30 combined loads and analyzed subsequently by H. E. Eisenmenger[3], and others, have given results that agree closely with these curves, except that Bary's curve for 30 combined loads terminates at 0 for $\phi \doteq 0$, whereas that of the author terminates at 0.1825 for $\phi \doteq 0$, for Δ_{30}. In the case for ϕ_{301}, in which the peak is measured on a one-hour basis, a load factor less than 0.0675 is impossible, for p_0 has been taken at 1/24 and $k=0.4\phi$.

The value $k=0.4\phi$ seems to apply closely enough for utility loads. It is not critical. For example, with $p_0=1/24$, $\phi=0.5$, and k taken as 0.2ϕ, 0.3ϕ, 0.4ϕ, and 0.5ϕ, respectively, the corresponding values for z using equation 7 would be 0.803, 0.792, 0.775, and 0.757, for $p=0.4$.

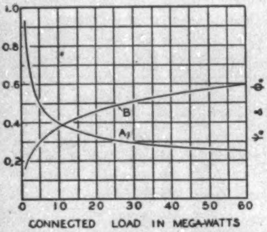

Figure 12. Demand factor and load factor versus connected load for military establishments

In this case, a 60 per cent decrease in the value of k from 0.5ϕ results in about six per cent increase in the value of z. Of interest too is the fact that a given percentage change in p usually causes less than one-half this percentage change in z.

Adding Loads of Various Load Factors

Equation 6, or others similar, which give consideration to the load-curve shape, can be used to find the value of a given load at any time p from the peak. Otherwise stated, if the time of a peak-load is known, the effect of this peak at any other time as added to any other peak or peaks can be estimated closely. This method is specifically applicable for a substantial number of peaks when their hour-to-hour characteristics are not readily obtainable, or where these vary and average values will suffice. It is not offered for a few curves as a substitute for the actual load curves if these are available. The curve for m/M, in equation 6, and others similar, is for a load duration curve with abscissas reversed. When used for direct-time readings of m/M, zero time may be taken at the midpoint of the peak load ($1/2\ p_a$) and the value of m/M would then be given by:

$$\frac{m}{M}=k+(1-k)\left[\frac{1-2p}{1-p_0}\right]^n$$

where

$$n=\frac{(1-\phi)}{(\phi-k)-p_0(1-k)} \tag{12}$$

Note that $2p$ is used instead of p when the equation is applied in this manner.

For Q loads occurring with a random distribution and having various load factors, the peak value of the sum of the loads may be found from:

$$M_Q=\Sigma(M\phi)+\left(\frac{1}{Q}\Sigma[M^2(1-\phi)^2]\right)^{1/2} \tag{13}$$

$$M_Q=\Sigma(Mz)+\left(\frac{1}{Q}\Sigma[M^2(1-z)^2]\right)^{1/2} \tag{14}$$

depending upon whether the peaks are distributed throughout the cycle of ϕ or concentrated during p so that z becomes applicable as heretofore discussed. These follow from equations 2 and 4.

The corresponding coincidence factors Δ_Q may be obtained from equations 13 or 14 by dividing M_Q by ΣM.

The Effect of Load Changes on Coincidence Factors and System Peaks

Coincidence factors have been employed to estimate the effect of an additional load on an existing peak load. It will be seen that this method can be applied only when the existing peak occurs during a time in which the additional load is apt to occur. If the time of a principal peak load has shifted, the relations which determine the relative contributions to this peak load are certainly altered. A single industrial customer, in the change from 8-hour to 16- or 24-hour operation, has correspondingly increased his load factor and, therefore, the hours during which his maximum demand (which may have remained constant) is likely to occur. The effect of a substantial number of such load changes would not necessarily increase a system peak. It might conceivably reduce it, at least alter its timing. Failure to understand or evaluate this Δ versus ϕ relationship and to foresee the limitations in materials and manpower, may in part account for many of the extremely high estimates which have been made from time to time of the total generating capacity needed for the war effort.

The solutions which are based upon probability relationships increase in accuracy as the number of loads increases. For a few loads, one may speak of the most probable solution. For a substantial number of loads, one may with more confidence speak of the actual solution for some of these intangible problems.

Demand Factors for Composite Loads

Since, for unit loads of all load factors, the load factor of the summation of a substantial number of such loads increases as the number of loads increases, it is apparent that the demand factor will decrease for given types of loads as the connected load increases. If the demand factor for Q loads is ψ_Q and the connected load is β_Q

$$\psi_Q=\frac{M_Q}{\beta_Q}=\frac{\Delta_Q\Sigma M}{\Sigma\beta}=\frac{\phi_1\,QM_1}{\phi_Q\,Q\beta_1}$$

$$=\frac{1}{\phi_Q}[\phi_1\psi_1]=\Delta_Q\psi_1 \tag{15}$$

Thus, for loads of a given load-factor type ϕ_1, ψ_Q is proportional to $1/\phi_Q$, since $[\phi_1\psi_1]$ is a constant value for that type of load. M_1/β_1 represents the kilowatt demand per kilowatt of connected load for the unit load, and, therefore, $[\phi_1,\psi_1]$ is the average load per kilowatt of connected load.

It has been determined that by dividing a given type of connected load into Q hypothetical unit blocks of 10, 100, or 1,000 kw each, useful empirical relations may be established for the demand-factors.

For example, for military establish-

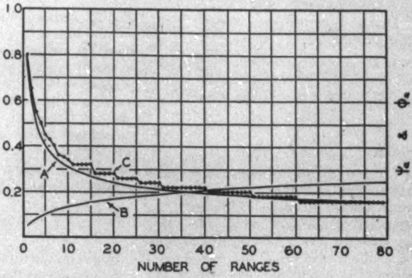

Figure 13. Demand factor and load factor versus number of electric ranges

$p=0.5$

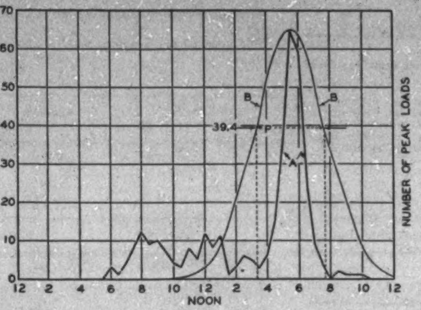

Figure 14. Frequency curve for individual electric-range peak loads

ments of certain kinds ranging in magnitude of connected load from 5,000 to 50,000 kw, the demands have been found to fall within or near the values given by the above relations with $p=1.0$, $\phi_1=0.159$, and $\psi_1=0.935$ for 1,000-kw unit blocks of connected load as given on curve *A* of Figure 12. Curve *B* of the same figure is for the corresponding load factors.[4]

Another example, for domestic electric ranges, is illustrated by curve *A* of figure 13 which represents demand factors versus number of ranges for $p=0.5$, $\phi_1=0.05$, $s=0.10$ (equation 9), and $\psi_1=0.8$. Curve *B* is for the corresponding load factors and curve *C* gives values of demand factors as given in the National Electric Code.[5]

Conclusion

The more one delves into this subject of load-curve analysis, the more apparent does it become that here is to be found an opportunity for research, particularly by utility companies, which should be profitable. It is hoped that in this brief paper, a few load-curve relations have been presented in a manner that will point to the possibilities for further investigations.

To explain more fully practical applications of some of the formulas presented, additional figures and a few examples are included in the Appendix.

Appendix

Symbols Employed in the Paper

ϕ—Load factor
Δ—Coincidence-factor
p—Peak-concentration factor (or peak-shift factor)
p_0—Peak-duration factor
k—Load-variation factor
s—Coincidence-factor for an infinite number of similar loads
Q—The number of loads
Δ_Q—Coincidence-factor for Q loads
M—Maximum value of a load
M_Q—Maximum value for the sum of Q loads
ψ—Demand factor
β—Connected load
α—An empirical number, value two or more
Σ—Sign of summation for terms following
T—Time of load cycle
t/T—Fraction of load cycle (in time)
m/M—Fraction of peak load (in power)

Explanation of Additional Figures

Figure 6 shows the s versus p relationship for various values of ϕ as calculated from equation 8, in which $k=0.4\phi$ and $p_0=1/24$.

Figure 7 shows the s versus p relationship for various values of ϕ, as calculated from equation 9, in which $k=0$ and $p_0=0$.

Figure 8 corresponds to Figure 6 except that the relationship of s versus ϕ of equation 8 is given for various values of p.

Figure 9 corresponds to Figure 7 except that the relationship of s versus ϕ of equation 9 is given for various values of p.

Figure 10 gives values of m/M versus hours of peak displacement for various values of ϕ as calculated from equation 12 with $k=0.4\phi$ and $p_0=1/24$.

Figure 11 gives values of m/M versus hours of peak displacement for various values of ϕ as calculated from equation 12 with $k=0$ and $p_0=0$.

Example 1

The ten loads shown in Table I are to be combined on a single circuit. What peak load may be expected at 6:00 p.m. and at 9:00 p.m.? Refer to equation 12 and Table II.

Example 2

What peak load may be expected on a power feeder which is to carry the loads shown in Table III? Peaks are on a 15-minute basis. In this case $p_0=1/96$. Take $k=0.4\phi$ and apply equations 7 and 14. See Table IV.

$$M_Q=3,436+\left(\frac{1,020,444}{10}\right)^{1/2}=3,755 \text{ kw}$$

$$\Delta_Q=\frac{3,755}{5,600}=0.67$$

(Note: average $s=0.633$)

Table I

Number	Kw Maximum	Kw Minimum	Time of Peak, P.M.	Peak Duration Minutes	Kilowatt-Hours Per Day
1	10,000	500	6:00	30	48,000
2	2,000	100	5:30	60	12,000
3	2,500	250	8:00	15	24,000
4	5,000	500	4:15	5	60,000
5	1,000	80	7:20	30	8,400
6	4,500	900	9:00	60	64,800
7	1,800	630	4:30	15	34,560
8	900	18	3:30	15	4,320
9	500	60	7:30	60	3,600
10	1,000	150	4:45	20	3,600
	29,200				

Table II

Number	ϕ	k	s	p_0	p, 6 P.M.	p, 9 P.M.	M, 6 P.M.	M, 9 P.M
1	0.20	0.05		0.0208	0.0000	0.1250	10,000	2,342
2	0.25	0.05		0.0417	0.0208	0.1458	2,000	611
3	0.40	0.1		0.0104	0.0833	0.0417	1,830	2,169
4	0.50	0.1		0.0035	0.0729	0.1979	4,204	2,859
5	0.35	0.08		0.0208	0.0556	0.0694	796	739
6	0.60	0.2		0.0417	0.1250	0.0000	3,636	4,500
7	0.80	0.35		0.0104	0.0625	0.1875	1,737	1,580
8	0.20	0.02		0.0104	0.1042	0.2292	327	69
9	0.30	0.12		0.0417	0.0625	0.0625	344	344
10	0.15	0.04		0.0139	0.0521	0.1771	410	63
							25,283 kw	15,276 kw

Peak load at 6:00 p.m. 25,283 kw
Peak load at 9:00 p.m. 15,276 kw

Table III

Number	Kilowatt-Hours Per Day	Kw Peak	Hours in Which Peak May Occur
1	1,200	100	9:00 a.m. to 9:00 p.m.
2	1,800	300	9:00 a.m. to 7:00 p.m.
3	8,640	600	8:00 a.m. to 8:00 p.m.
4	1,440	200	10:00 a.m. to 10:00 p.m.
5	6,000	1,000	4:00 p.m. to 10:00 p.m.
6	9,600	2,000	5:00 p.m. to 10:00 p.m.
7	13,440	700	12:00 m. to 12:00 m.
8	1,080	50	12:00 m. to 12:00 m.
9	1,440	400	5:00 p.m. to 11:00 p.m.
10	600	250	4:00 p.m. to 8:00 p.m.
	42,240	5,600	

Table IV

Number	ϕ	p	z	Mz	$M^2(1-z)^2$
1	0.5	0.500	0.699	70	906
2	0.4	0.417	0.665	200	12,800
3	0.6	0.500	0.781	469	17,266
4	0.3	0.500	0.468	94	11,321
5	0.25	0.250	0.592	592	166,464
6	0.2	0.208	0.563	1,126	763,876
7	0.8	1.000	0.800	560	19,600
8	0.9	1.000	0.900	45	25
9	0.15	0.208	0.458	183	4,700
10	0.10	0.167	0.387	97	23,486
			Average z = 0.633	3,436	1,020,444

Example 3

Tests made on a few electric ranges of a given type indicate an average maximum demand of 3.5 kw for one range, and a monthly consumption of 125 kilowatt-hours. At 80 per cent transmission efficiency, what will be the increase in a utility company's 7:30 p.m. peak load if the maximum probable use of the ranges occurs at 5:30 p.m. and 10,000 of these ranges are to be placed on the system?

Solution

The load factor for one range $\phi = 125$ kilowatt hour per month − range/720 hours per month × 3.5 kilowatts per range = 0.0496.

The peak-time-concentration factor $p = 0.188$ (from equation 10).

The coincidence factor for an infinite number of ranges (or the load factor per range for the time-concentration factor p) = 0.26 (from equation 9).

The coincidence factor for 10,000 ranges Q is $\Delta_Q = 0.2674$ (from equation 5).

The demand for 10,000 ranges Q is $M_Q = \Delta_Q Q M_1/0.8$ for 80 per cent efficiency. So that

$M_Q = (0.2674)(10{,}000)(3.5)/0.8 = 11{,}699$ kw, 5:30 p.m. range peak.

Now the load-factor for Q ranges, $\phi_Q = \dfrac{\phi}{\Delta_Q} = 0.1865$.

The peak-time-shift factor, $p_s = 2$ hours per day/24 hours per day = 1/12.

The fraction of the range peak at 7:30 p.m. is $m/M = 0.447$ (equation 12).

So the range load contribution at 7:30 p.m. is $m/M \times M_Q = 5{,}229$ kw.

Example 4

Neglecting transmission losses, what peak demands per heater may be expected for groups of 5, 10, 20, 170, and 500 single-unit domestic-type water heaters of 1,500 watts rating, considering that the maximum use will occur between 7:30 a.m. and 8:30 p.m. and the average monthly consumption per heater is 300 kilowatt-hours?

Solution

The load-factor for one heater $\phi = 300$ kilowatt-hours per month per unit/720 hours per month × 1.5 kilowatts per unit = 0.278.

The peak-time-concentration factor $p = 13$ hours per day/24 hours per day = 0.542.

The coincidence-factor for an infinite number of heaters (or the load-factor per heater for the time-factor p) is

$$z = \frac{\phi}{p}\left\{1-(1-p)^{1/\phi}\right\} = 0.482$$

The coincidence-factor for Q heaters $\Delta_Q = z + (1-z)Q^{-1/2}$.

The demand per heater is $\Delta_Q M_1 = 1.5\ \Delta_Q$ so that:

Q	Δ_Q	Kilowatts Per Heater
5	0.714	1.071
10	0.646	0.969
20	0.598	0.897
170	0.522	0.783
500	0.505	0.758

The reader is invited to compare these results with Figures 14 and 16 of reference 6, which were based on a large number of tests.

Example 5

A proposed military post for 40,000 officers and men is expected to have the wintertime loads shown in Table V.

For purposes of determining the capacity of the service feeders and transformers, find the maximum load, the average load over a three-hour period, and the rms value over three hours.

Solution

See Table VI. The quantity p is from equation 10, and z is from equation 9.

$$M_Q = 3{,}477 + \left(\frac{464{,}679}{3}\right)^{1/2} = 3{,}871 \text{ kw}$$

maximum demand

M_Q is from equation 14.

$$\phi_Q = \frac{33{,}200}{24 \times 3{,}871} = 0.357$$

For a displacement of $1^1/_2$ hours from the peak load:

$m/M = 0.717 = k_2$ (using equation 12 with $k = 0.4\phi$ and $p_0 = 0$)

$z_2 = 0.852$ (using equation 7 with $k = 0.4\phi$ and $p_0 = 0$)

z_2 is the load factor over the three-hour period. So that average value over three hours = 3,871 × 0.852 = 3,297 kw.

Table V

Type	Connected Kilowatts	Daily Use, Kilowatt-Hours	Hours Use of Maximum Demand
1. All lighting	4,100	16,000	8
2. Heating and cooking	4,000	12,000	6
3. Power and miscellaneous	1,500	5,200	10
	9,600	33,200	

Table VI

	ϕ	p	z	M	Mz	$M^2(1-z)^2$
1	0.333	0.226	0.791	2,000	1,582	174,724
2	0.250	0.202	0.735	2,000	1,470	280,900
3	0.417	0.273	0.817	520	425	9,055
					3,477	464,679

To find the rms value of load use equation 5 of reference 1. Thus

$$\theta = z^2 + \frac{(z_2 - k_2)^2}{\left(\frac{1-k_2}{1-z_2}\right)^2 - 1} = 0.7945$$

where θ is the loss factor, so that rms value over the three-hour period is $\sqrt{\theta}M = 3{,}450$ kw.

The peak time-concentration factor p, as used in the foregoing example, is an "equivalent time" in which the total number of peak loads may be considered as concentrated.

Actually, in certain types of loads, there may be a frequency distribution of these peak loads which can be represented graphically by a frequency polygon of peak concentration about some given time at which the density of the peak concentration will be a maximum value or mode of the frequency distribution. While not conclusive, it would seem from a limited number of cases that p may be taken as that time (or fraction of the total time of a cycle) which, if squared and multiplied by the total number of peak loads, will give the sum of the second moments about the axis of the mode of the frequency polygon. Abscissas in this polygon would represent times of the class intervals of the peak loads and ordinates would be the numbers of peak loads falling within the various frequency rectangles.

Mathematically, $p \doteq [Q^{-1}\Sigma(p_cQ_c)]^{1/2}$ for the frequency polygon and, if the number of loads becomes large, $p \doteq 1/\sqrt{2}\,h$ where h is the modulus of the probability curve relating p_c and Q_c, thus

$$Q_c = Q_m \epsilon^{-(hp_c)^2}$$

and $p \doteq \sigma$, the standard deviation.

Refer to Figure 14 which shows on curve A the peak-load-time distribution for 50 nine-kilowatt electric ranges found in a recent test in Philadelphia over a period of one week, as furnished by Constantine Bary.

Curve B is the probability curve whose standard deviation is equal to the $[Q^{-1}\Sigma(p_cQ_c)]^{1/2}$ of curve A, namely 4.44 hours or, as a fraction of one day, $p = 4.44/24 = 0.185$. Compare this with the value of p in example 3 which was taken from equation 10.

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Electrically Operated Gyroscopic Instruments

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THE topic "Electrically Operated Gyroscopic Instruments" covers a very wide and interesting field in which our work includes, for the most part, instruments and systems closely associated with aircraft, navigation, and control.

Gyroscopic instruments for aircraft originally were air-operated because of the availability of pneumatic pressure from the simple Venturi tube. As the use of electricity aboard airplanes became prevalent, the advantages of electrical operation were applied to the gyroscopic instruments.

The electrically driven gyroscope may be divided into the three commonly used types:

1. The vertical gyroscope.
2. The directional gyroscope.
3. The rate gyroscope.

Since the vertical gyroscope is used for stabilizing the directional reference, the directional gyroscope may be disregarded in this discussion.

Gyroscopes may be classified under the two phenomena associated with rotating bodies:

1. Stability or the fixity of the spin axis in space.
2. Precession or the reaction of the spinning body to a torque about an axis other than the axis of rotation.

Gyroscopes are characterized by the requirements of the maximum moment of inertia about the rotational axis and maximum rotational velocity. The equation $T = I\Omega\omega$ defines the relationship between applied torque, moment of inertia, precessional velocity, and spin velocity in either the vertical or the rate gyroscope. In both types the rotor is designed to provide the largest possible moment of inertia and is constructed, so that it is exterior to the stator. For most purposes the gyroscope is an induction motor designed to operate at 400 cycles from single- or three-phase supply, 110 or 26 volts. Rotor speeds for the large gyro are 10,000 rpm and for the smaller units 20,000 rpm. Power consumption is between four and six watts.

Vertical Gyroscopes

In the vertical gyroscope (see Figure 1) the equation $T = I\Omega\omega$ defines the rigidity of the gyroscope. T is an index of the amount of torque which the gyro can resist in the form of friction and disturbing forces. Its importance in the field of aircraft control has been occasioned by the necessity for a fixed reference, regardless of changes in the aircraft's attitude. High velocities, accelerations, pitch, bank, poor visibility, and great altitudes have made it imperative that fixed axes be established to provide reference for control, navigation, bombing, fire control, and so forth. For the purpose of establishing attitude, a stable vertical reference is required. The gyroscope possesses the necessary stability, but it must also be responsive to the earth's gravitational field, so that it may become vertical. A free gyroscope established in the vertical soon would depart from that position because of such factors as precession caused by friction, departure from vertical caused by the earth's rotation, and departure caused by the motion of the craft on the earth's surface. To insure that the gyroscope remains vertical, a device called an erection mechanism (see Figure 2) causes the gyroscope at all times to precess into the vertical axis. The response of the gyroscope and its erection mechanism to the gravitational field cannot be separated from its response to any other acceleration. The problem of seeking the vertical, therefore, is one of separating gravitational acceleration from random accelerations and from those introduced by maneuvers of the craft. Since it is impossible to separate them instantaneously, the combination of the gyroscope and its erection mechanism is so designed that the response of the system to accelerations is very slow. Under these conditions the random accelerations will cancel out over a period of time, while those caused by maneuvers are responded to so gradually, that departure from the vertical, even in extreme cases, is small. Since the gravitational acceleration is constant and continuous, the gyro is at all times corrected to the vertical during normal, straight, and level flight.

Basic Gyro Vertical

We have three gyroscopic elements characterized by different sizes of gyrorotors. The gyro vertical (see Figure 3)

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consists of an induction motor in which the rotor comprises the gyroscopic mass and is external to the stator to secure maximum moment of inertia. The axis of rotation of the motor is normally vertical. Attached to the housing is the erection mechanism. The rotor, stator, housing, and erection mechanism comprise the gyromotor. This gyromotor assembly is supported on ball-bearing pivots in a gimbal ring, which in turn is supported on ball-bearing pivots in the main housing. The axes of the gimbal support are perpendicular to each other and to the rotor axis. It follows that the two gimbal axes are normally in a horizontal plane. The gyromotor is in a universal joint, and the gyro axis is free in two horizontal axes about its rotational axis. The basic gyro vertical therefore can be described as a gyroscope with three degrees of freedom provided with an erection mechanism.

The erection mechanism (see Figure 2) consists essentially of a steel ball rolling in a circular track. A disk with a slot is mounted above the track allowing the ball to run freely within the limit set for it by the slot. The ball on this track acts as a level and reacts to the inclination of the track, which is perpendicular to the rotor spin axis. A circular magnet attached to the gyrorotor shaft turns inside a drag cup attached to cause the disk to turn. The magnetic coupling is designed so that the disk turns at approximately 30 rpm.

When the gyro axis is vertical the ball travels with uniform velocity, and there are no unbalanced forces. Displacing the gyroscope inclines the track, causing a nonuniform ball velocity due to acceleration. An escapement mechanism, built into the disk, is provided to restrain the speed of the erection system when the gyroscope is in the vertical position and still provide sufficient torque for correct operation of the mechanism when the gyroscope is displaced from the vertical.

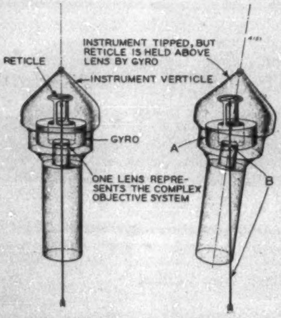
RETICLE
INSTRUMENT TIPPED, BUT RETICLE IS HELD ABOVE LENS BY GYRO
INSTRUMENT VERTICLE
GYRO
A
ONE LENS REPRESENTS THE COMPLEX OBJECTIVE SYSTEM
B

Figure 1. Principle of vertical gyroscope (applied for driftmeter)

A. Schematic gyroscope shown hollow to allow light to pass through, although it is solid in the instrument, and the light is reflected aside by a prism

B. Although the lens is tilted, it still directs the vertical ray to the center of the reticle

The mechanism is so designed and balanced that the rotating mass of the ball, due to its variable velocity, exerts a precessing torque upon the gyroscopic system at an angle of 90 degrees from the inclined angle of the gyroscope to the vertical. The chief consideration is that the variable velocity of the rolling ball creates an erecting torque on the gyroscope which persists until the track is horizontal, at which time the gyroscope is vertical, the ball speed is uniform, and no further unbalanced forces exist. In all three of the gyro verticals power is supplied through the gimbal suspension by means of hairsprings or insulated pivots. The selection of rotor and gimbal bearings is an important problem, since these instruments must function through a temperature range of from -54 degrees centigrade to $+70$ degrees centigrade; the available torque exerted on the gyroscope is minute and any imperfection in the gimbal bearings will result in failure to attain required accuracy to vertical.

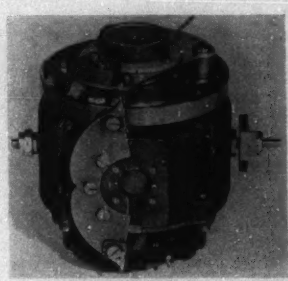

Figure 2. Vertical gyroscope showing erection mechanism

Precessional Gyroscope

The characteristics of the precessional gyroscope (see Figure 4) are given again by the equation $T=I\Omega\omega$ in which T is the precessional torque, I is the rotor moment of inertia, Ω is the angular velocity to be measured, and ω is the angular velocity of spin. It is evident from the equation that an angular velocity imposed on a spinning mass will result in a torque which is proportional to that angular velocity. The precessional or rate gyromotor is similar in construction with the gyro vertical in that it consists of an induction motor comprising an internal stator, an external rotor of maximum moment of inertia, a motor housing, and a set of pivots in gimbal bearings attached to the external housing. The basic-rate gyro can be described as a restrained gyroscope with two degrees of freedom. Angular velocity about the reference axis causes a precessional torque about the restrained axis. The torque acts against appropriate springs, causing a displacement proportional to the imposed angular velocity. In a specific case, the rate of turn indicator for aircraft, the rotor axis is in a horizontal plane athwarthship, the gimbal axis is in a horizontal plane along the fore and aft axis. The rate of turn to be measured is about a vertical axis, and the precessional torque is exerted about the fore and aft axis against restraining centralizing springs.

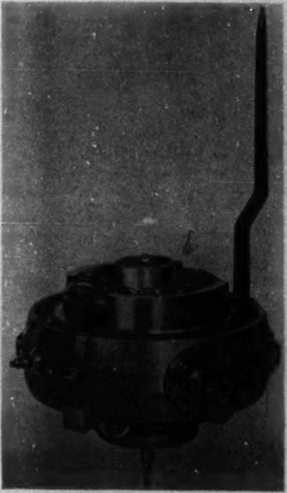

Figure 3. Basic vertical gyroscope (applied for gyro roll indicator)

Production Problems

The imposition of severe restrictions in space and weight on instruments designed for aircraft requires the use of components of small size and extreme precision. For example, decreasing the length of an axis by 50 per cent automatically calls for a 50 per cent reduction in allowable tolerance to maintain the same angular accuracy.

The problems attending the production, inspection, and assembly of small parts of high precision have forced the adoption of methods of manufacture, such as coining, centrifugal casting, and forging in extreme accuracy and small size. Accuracy requirements have resulted in adoption of production techniques capable of maintaining ±0.0001-inch tolerances. Assembly and calibration of these instruments necessitates a technique where the microscope and the vacuum-tube amplifier are essential parts of production and inspection equipment. Dynamic balancing of the small high-speed rotors required the development of a production balancer of novel design in which the rotor is operated, and the location of unbalance in either of two correction planes is marked at the location of metal to be removed by means of an electric arc. An indication of required depth to be drilled is secured and the rotor balanced by drilling out the prescribed amount at the marked spot by means of an integral drilling fixture on the machine. In this manner rotors of several ounces in weight are balanced to a residual unbalance not greater than 0.00001 ounce-inch.

The assembly of these instruments must be done in air-conditioned rooms with extreme emphasis on constant temperature, freedom from moisture, dust, and contamination. Investigation of sources of contamination indicate necessity for eliminating even the handling of parts with fingers and the development of technique, whereby vital parts are never touched with the hands. The use of cleaning methods and cleaning materials has been investigated fully to the extent that special equipment and specially prepared washing solutions have been attained and held to rigid specifications. The problem of testing, cleaning, and lubricating ball bearings was, in itself, a project of considerable scope requiring the development of highly specialized equipment and production techniques.

Gyro Horizon Indicator

This instrument may be used to fulfill two purposes. It serves to transmit signals in pitch and bank for automatic pilot control, or it may serve as a visual reference to the human pilot indicating the attitude of the plane. The unique feature of this instrument is its small size. Heretofore, the artificial horizon has been of larger size due to difficulties of manufacture and required accuracy. With the development of the small gyro vertical we have been able to produce an instrument of standard interchangeable size with other aircraft panel instruments (that is, $3^1/_8$ inches in diameter), which fulfills the requirements of accuracy for both indication and autopilot control. The gyroscope consists of an induction-type motor driven by three-phase 26-volt current supply. The motor draws five watts. The rotor of this instrument has a speed of 20,000 rpm. To secure the highest-possible moment of inertia, a centrifugally cast silver-alloy rotor with an integral squirrel cage is used. The gyromotor is mounted in a fork-type gimbal ring which, in turn, is mounted on a main pedestal in the rear of the instrument housing. Transmission of pitch and bank signals is accomplished by small signal generators on the pitch and bank axis. Conductors supplying power to the gyromotor and bringing out electrical signals are provided in the combination of contact pins and hairsprings. Some conception of the problems involved in the development of this instrument may be glimpsed from the fact that within the space of $3^1/_8$ inches in diameter by 6 inches in length is provided a gyro vertical of accuracy within 20 minutes of absolute vertical with freedom of ±100 degrees in bank, 80 degrees in dive, and 70 degrees in climb, transmitting signals of attitude in pitch and bank.

Figure 4. Principle of precessional gyroscope

Driftmeter

The gyro-stabilized driftmeter (see Figure 5) is an optical instrument designed to provide the navigator or bombardier with a stable reference with which to compute drift, ground speed, and bearings of terrestrial landmarks from the airplane independently of the pitch and roll of the aircraft.

The driftmeter consists of a vertical telescope through which the ground may be viewed and upon which is superimposed the image of a gyroscopically stabilized reticle. The system is so designed that, with pitch and roll of the aircraft, there is no relative motion between the image of the ground and the reticle, and so an observer can see what is apparently a stationary image against the reticle. The motion of the ground due to flight can be referred to reference lines on the reticle and accurate readings of drift obtained. The telescope is provided with scales in azimuth and in elevation, so that for certain purposes accurate bearings may be obtained. The gyroscopic element of the driftmeter is the large gyro vertical, consisting of the rotor of 5,000 grams centimeters squared moment of inertia, a rotor speed of 10,000 rpm, power supply of 400 cycles, 110 volts, single phase, and power consumption of approximately seven watts. This gyroscope does not contain any signaling devices, and current is brought into the gyromotor through the gimbal rings by means of insulated pins in hollow pivots concentric with gimbal bearings. The gyro is calibrated to an accuracy of better than ten minutes from true vertical.

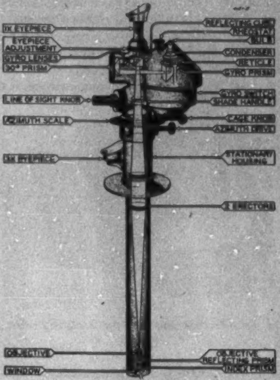

Figure 5. Driftmeter

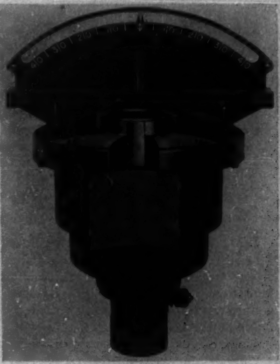

Figure 6. Gyro roll indicator

Gyro Roll Indicator

The roll indicator (see Figure 6) is an example of the application of an aircraft type of gyro vertical to marine use. For the purpose of fire control on board a medium-size vessel it was necessary to provide an accurate indication of the ship's roll with respect to true vertical. For this purpose the gyro used in the driftmeter was adopted. A special housing is provided and a pointer attached to the gimbal ring, the pointer being read against a scale on the stationary housing. The instrument is mounted with the outer gimbal bearing supports along the fore and aft axis of the ship and a reading of roll of the vessel is obtained within an accuracy of better than one-half degree under conditions of rough weather and maneuvers.

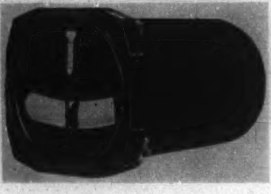

Figure 7. Electric turn-and-bank indicator

Electric Turn-and-Bank Indicator

The turn and bank indicator (see Figure 7)is an instrument which combines the two functions that indicate the rate of turn of an aircraft and the resultant acceleration which is, in general, referred to as bank. The combination of these two indications results in correctly executed turns at proper banking angles. This instrument has been developed in three basic types:

1. Utilizing an air-driven gyroscope.
2. A d-c-driven gyroscope.
3. An a-c-driven gyroscope.

The principles and basic structure of the rate gyroscope have been described already under the heading "Precessional Gyroscope." In the d-c-driven instrument the gyroscope consists of a small d-c motor on the shaft of which is mounted a flywheel of sufficient moment of inertia to provide the required indicating characteristics against the centralizing springs. The motor current is supplied to the 24-volt shunt motor through contact pin connections to the case.

The a-c-rate gyroscope consists of an induction motor of which the rotor is the gyro mass. The rotor is exterior to the stator and rotates at a speed of 20,000 rpm. Power supply is 26 volts, three phase, 400 cycles at approximately four watts. The instrument serves as an indicator and also as a transmitter for aircraft control. At one end of the pivot axis is located a signal generator which provides electrical signals as a function of the angular velocity of the aircraft. Power supply and signal leads are in the form of hairsprings from gyroscope to case.

Gyro Flux Gate* Compass System

The Gyro Flux Gate compass system is a remote indicating earth-inductor gyro-stabilized compass. The system is composed of a gyro-stabilized Flux Gate transmitter, a master indicator, an amplifier, and repeater indicators. Its advantages are based on the fact that the magnetically sensitive compass element or Flux Gate is located in a position free from the magnetic and electrical disturbances usually found in the pilot's and navigator's stations, and that the Flux Gate is gyro stabilized and is thus maintained in a horizontal position at all times. As a result of this stabilization, turning and acceleration errors, for all practical purposes, are eliminated.

The Gyro Flux Gate transmitter (see Figure 8), with which we are concerned primarily, is a gyro vertical of the larger size, similar in electrical characteristics to the driftmeter type. Operating power is supplied at 110 volts, 400 cycles, 6 watts to the gyro induction motor. The motor stator is, again, inside the rotor and consists of a two-phase four-pole winding, one of the phases being in series with capacitors mounted on the gyromotor to provide the necessary quadrature current. In the bottom of the gyromotor housing is mounted a magnetic detector sensitive to the direction of the magnetic field in its plane. As a result, steady and accurate compass readings are obtained independently of pitch, bank, and maneuvers of the aircraft.

Automatic Pilot

This system comprises a combination of units, the purpose of which is the control of an airplane in attitude, direction, and through selected maneuvers. In the application of electrically driven gyroscopes to the automatic pilot, three of the instruments previously described are used. These are the artificial horizon, the Gyro Flux Gate transmitter, and the turn and bank indicator.

The artificial horizon transmits electrical signals of the attitude of the airplane with respect to the vertical to suitable devices for the purpose of control. These devices consist of amplifiers and servomotors which actuate the control surfaces of the airplane and maintain attitude.

The gyro-stabilized Flux Gate transmitter is also a two-purpose instrument in that it serves to transmit the magnetic heading of the aircraft for the purpose of indication and to provide a directional reference through amplifier and servosystem to the rudder-control surface.

The turn-and-bank indicator, likewise a dual-purpose instrument, serves to indicate the rate of turn-and-bank angle of the aircraft and to transmit the rate signals to combine with the directional signal of the Gyro Flux Gate system in such a manner that precise directional control is obtained. The rate signal is used further to control the desired rate of turn and to execute precision turns of correct banking angle.

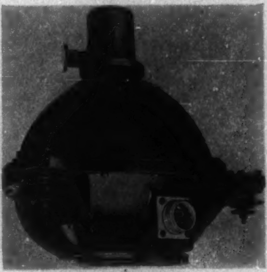

Figure 8. Gyro Flux Gate compass transmitter

To summarize, the electric gyroscope in aircraft instrumentation is rapidly supplanting air-driven types, owing to the following factors:

1. Freedom from air-supply requirements at high altitudes.
2. Sealing and elimination of air supply, thus preventing contamination and insuring greater reliability.
3. Greater versatility, especially where electrical signaling devices are required.
4. Compactness, greater reliability under extreme temperatures due to speed regulation and torque characteristics.

Since the development of electrically driven gyroscopes for instrumentation is a relatively new art, many interesting and very important applications still are to be anticipated.

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The Copper-Oxide Rectifier in Electrochemical Work

I. R. SMITH
ASSOCIATE AIEE

MUCH has been written about the copper-oxide rectifier during the 23 years of its existence, yet the subject is far from exhausted. It still remains one of the most interesting developments of recent years. A brief résumé of its history may be of interest.

The scene opens in 1921 when L. O. Grondahl discovered that a large area of copper coated with cuprous oxide would act as a rectifier. Five years later the rectifier known as the Rectox was introduced commercially for use in trickle charging "A" batteries in home radio sets. This became a tremendous business overnight but disappeared with the same disconcerting speed two years later with the advent of a-c tubes. Application of the rectifier to industrial uses started in 1927 and after a period necessary for trial and acceptance by users began to develop rapidly, reaching its peak during the past three years in many diversified war activities.

Though this may be familiar material to many, it will be worthwhile to run through those characteristics which particularly apply to the use of the rectifier in electrochemical work. A copper-oxide rectifying element is an integral unit consisting of a disk or plate of high-purity copper, coated on one or both sides with red (cuprous) oxide. The combination is shown schematically in Figure 1. It is an asymmetric resistor, that is, it passes current better in one direction than in the other. Current flows readily from oxide to copper, but with difficulty from copper to oxide.

This is best illustrated by a typical volt-ampere curve, which is simply a curve showing how much current flows in either direction for any given voltage, Figure 2. To illustrate, if one applies three volts in the low resistance direction, a current of nine amperes will flow through a $1^1/_2$-inch-diameter disk. If the voltage is then reversed, the current flow will be perhaps one milliampere, a ratio of 9,000 to 1.

From the shape of the curve, one can see that resistance in either direction is not a constant. This is better shown by Figure 3, on which is shown the variation in resistance with voltage for both directions of polarity.

These characteristics do not remain fixed but vary with temperature and also with time and operation. The forward resistance will tend to increase over a long period of time, the amount depending upon the temperature, while the back resistance will decrease somewhat during operation. These effects are known as aging. The effect of temperature is to cause both forward and back resistance to decrease as temperature increases, and vice versa.

The construction of copper-oxide rectifier units is by now familiar to many users. There are two general types, one involving a high degree of pressure on the rectifying element, the other requiring practically no pressure.

The pressure-type construction is applied to disk rectifiers used for relatively small capacities. A variety of such units is shown in Figure 4. The rectifying disks are strung on insulated bolts, with thin lead washers placed against the oxide surface. Radiating fins and metal spacers may be added, and the whole structure then pulled together under a pressure of over 1,000 pounds per square inch. This gives a solid, rugged type of unit that has stood the test of all kinds of service for many years.

The other type of construction is employed on plate-type rectifiers. A cross-sectional view is shown in Figure 5. The rectifier plate, to begin with, has oxide on both sides, whereas the disk is coated only on one side. The plate oxide is then reduced to copper on its outer surface, and the copper is plated over with nickel. Contact then must be made to both nickel surfaces. This is done with large spring washers, insulated from the plate copper, and pulled down and riveted together with a metal bushing. This construction thus parallels the two sides of the plate and furnishes the negative connection. The copper or positive connection is obtained by removing the oxide around a second hole through the plate. Figure 6 shows three sizes of plates with their connectors.

Plate rectifiers are used with natural convection cooling, but more often with forced draft. The plates are assembled on insulated studs, about one-half inch apart. This is ideal for forced cooling, as the cooling air comes in direct contact with nearly all of the active surface.

From one point of view, the method of determining a rectifier rating is of no interest to a user since he buys the rating he wants and expects that the manufacturer will have determined the rating properly. However, a user who does know more about the unit than just what is stamped on the name plate, can certainly operate with greater intelligence and with less likelihood of getting into trouble.

The method used is very simple to understand but the application of it is a matter involving considerable time and work. That, however, is the designer's problem.

Basically, the rectifier is a thermally rated device, like a transformer or a motor. Due to its internal resistance,

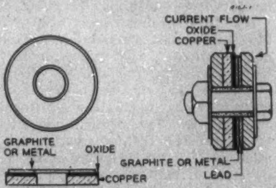

Figure 1. Diagrammatic cross section of single copper-oxide disk

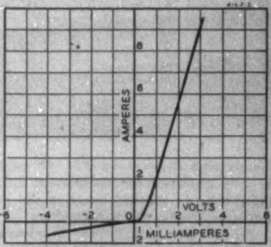

Figure 2. Typical volt-ampere curve for single copper-oxide disk

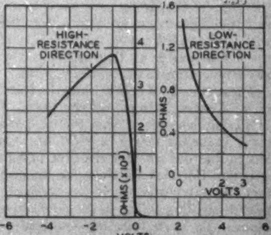

Figure 3. Typical resistance-voltage curve for single copper-oxide disk

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Paper 44-163, recommended by the AIEE committee on electrochemistry and electrometallurgy for presentation at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944. Manuscript submitted April 14, 1944; made available for printing May 16, 1944.

I. R. Smith is section engineer, Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.

Figure 4. Group of Rectox disk-type units

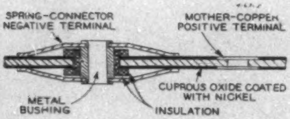
SPRING-CONNECTOR NEGATIVE TERMINAL
MOTHER-COPPER POSITIVE TERMINAL
CUPROUS OXIDE COATED WITH NICKEL
METAL BUSHING
INSULATION

Figure 5. Diagrammatic cross section of Rectox plate

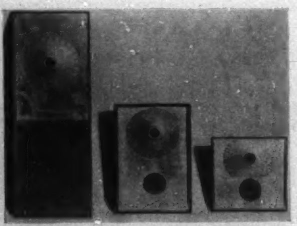

Figure 6. Standard copper-oxide plates with connectors assembled

heat is generated by the flow of load current in the forward direction and of leakage current in the back direction.

For any given rating, then, these losses are determined separately, either from volt-ampere curves or from actual tests, covering a range of operating temperatures. The separate losses are then combined into a total loss curve. In Figure 7 the general shapes of both the forward and back loss curves are shown, together with the total loss curve, which tells what the actual loss is at that rating and at any temperature in the operating range.

These curves, of course, must be based on aged characteristics, not new ones.

Having determined what the total loss curve is for the rating in mind, one still has to find out whether the type of construction intended to be used is satisfactory, that is, whether it has sufficient heat-dissipating ability to get rid of these losses. To learn this, the temperature rise of the particular structure is determined as a function of the watts to be dissipated in the structure. This gives a heat-dissipating curve which can be matched with the total loss curve to show what is the right combination.

How this works is shown in Figure 8. The heat-dissipating curve for the unit is curve *D*, drawn with the ambient temperature as a starting point. Also shown are three total loss curves. These are for the same total output. Curve *A*, however, is the total loss in a rectifier operating at high voltage and low current, curve *B* for one operating at high current and low voltage, and curve *C* for median values of voltage and current. Curve *C* corresponds to actual commercial ratings. Inspection of this figure shows that the combination of *A* and *D* is unsatisfactory. The two curves do not intersect and the rectifier would consequently burn up at this rating. Curve *B* intersects *D* at a broad angle and is obviously very safe since if temperature rises for any reason, losses will actually decrease. Curve *C* is also quite safe, crossing *D* at a reasonably wide angle.

Since curves *A*, *B*, and *C* are for the same output, their relative positions indicate the relative efficiencies. Curve *B*, it can be seen, has obtained exceptional safety at the expense of efficiency. Curve *C* gives good efficiency and satisfactory safety with a margin in case of occasional operation at higher than rated ambient temperature. Curve *C* also gives approximately the same losses and efficiency over the entire range of temperature, which is desirable. It follows also that the operating temperature, which is at the intersection of the loss curves and the heat dissipation curve *D*, is much lower for curve *C* than any other rating.

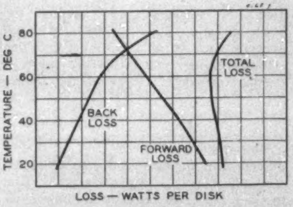

Figure 7. Relation of internal losses to unit temperature

With this brief discussion of characteristics, construction, and ratings, attention may be turned to applications, particularly in electrochemical fields.

Of course, the commonest of all electrochemical rectifier applications is battery charging. Probably nothing needs to be said beyond the fact that copper-oxide rectifiers are used successfully in all types of battery charging, ranging from a single cell at a fraction of an ampere to a 2,500-ampere 12-volt charger. Of interest is the 12-cell one-ampere oil-immersed charger shown in Figure 9. The use of oil immersion for protection against corrosive atmospheres is likely to expand rapidly. Figure 10 is included to show a fully automatic charger for industrial-truck batteries. This charger will handle up to 18 cells of lead-acid battery, and up to 450 ampere-hours capacity. The charger automatically gives the battery its proper rate, regardless of the state of charge. At the correct point, the rate is reduced to the desired finishing rate, and at the end of the finish charge, the rectifier shuts down and disconnects the battery. A companion model is available for nickel-alkaline batteries.

The next application is not so widely known except in the oil industry. That is the cathodic protection of pipe lines. It was found about 15 years ago by R. J. Kuhn and others that pipe-line leaks could be prevented if the pipe could be maintained cathodic, that is, negative to the surrounding soil. This was reasonable, since leaks occur due to loss of metal where the pipe is anodic, the soil being an electrolyte. To make the pipe cathodic, that is, to force it to be negative to the earth at every point, a source of direct current was connected between the pipe and ground plates in the surrounding earth. The system is shown simply in Figure 11. When such an installation is properly designed, the result is complete stoppage of leaks. The requirements are for relatively small-capacity units, delivering up to say ten volts at up to 100 amperes, the units being mounted on poles or platforms along the pipe line at intervals of the order of a mile. Operation, of course, is continuous.

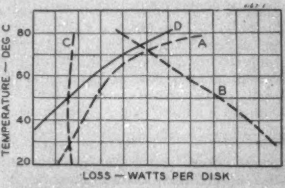

Figure 8. Heat-dissipation curve compared to total-loss curves for different disk ratings but same unit output

For this work the copper-oxide rectifier has been used since the beginning. It has good efficiency, which is important since operation is continuous and power rates are sometimes quite high. It can be mounted in a weatherproof box for outdoor service. It has no moving parts and requires no maintenance, and will operate for long periods without any need for adjustment. Figure 12 shows one of the many thousands of such units in service. Figure 13 illustrates a fan-cooled Rectox built for this type of work for outdoor service.

A kindred application is the protection of water tanks and similar structures. Figure 14 shows a diagram of a typical installation. Electrodes placed in the water maintain a protective film of hydrogen over the inner surface of the tank, preventing corrosion and eliminating the need for painting. The units are of small capacity costing little to operate, and the installation as a whole is easy to justify economically. The requirements are similar to those for cathodic-protection rectifiers.

In the chemical and metallurgical field, electrostatic separators and Precipitrons are frequently used. These require a high-voltage source of small direct currents. Figure 15 shows a power supply for a metal separator which delivers 15,000 volts at a few milliamperes to the electrodes. The use of a copper-oxide rectifier is attractive in this field because of the elimination of tubes and tube replacements. The higher first cost of the copper-oxide unit is felt to be justified by the freedom from later maintenance and the cost of tubes.

In the field of anodizing and plating, rectifiers have reached great prominence during the war. This has resulted to quite an extent from the shortage in rotating equipment, so that many people had to use rectifiers for plating and anodizing who otherwise probably never would have tried them. Now that they have gained familiarity with rectifier operation and its advantages, it is believed rectifiers will be preferred for many plating applications.

In Figure 16 is shown a standard stock rectifier for plating work, rated six volts, 500 amperes maximum. It is fan cooled, takes less than 4 square feet of floor space, stands 38 inches high, and weighs about 600 pounds. It is designed so that one or two more units can be mounted on top, thus economizing on floor space for larger capacities. These units are connected in parallel or series, as desired, for larger outputs, and are controlled from a separately mounted step-type regulator. The regulator unit then is mounted close to the operator, while the rectifier cubicles are located where they are not subject to corrosive fumes. The units being fan cooled are exposed to large amounts of room air during their operation and cannot be used safely in a corrosive atmosphere.

The unfortunate effect of corrosive atmospheres on copper-oxide plates (in which in fact they are no different from other types of electric apparatus) led to the design of a totally enclosed rectifier which could be installed right in the plating room adjacent to the plating tanks. This type of rectifier was supplied for two continuous strip-tinning lines. The rectifiers are mounted two high and are right next to the plating tanks, which are also on two levels. This resulted in a

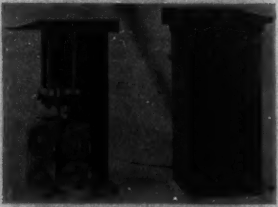

Figure 9. Oil-immersed Rectox battery charger
Rating one ampere, 12 cells

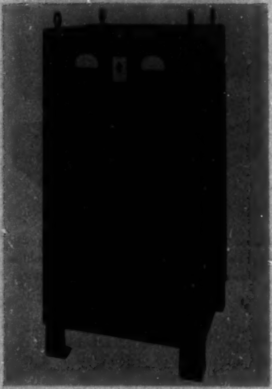

Figure 10. Automatic charger for industrial-truck batteries

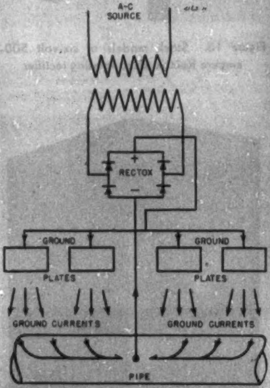

Figure 11. Diagrammatic representation of cathodic-protection system for pipe lines

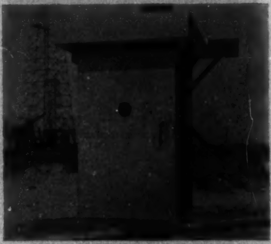

Figure 12. Installation of pole-mounted Rectox for cathodic protection

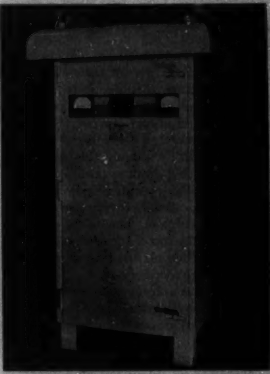

Figure 13. Outdoor fan-cooled plate-type Rectox for cathodic protection

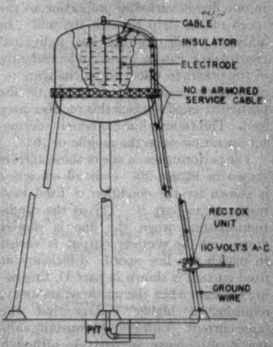

Figure 14. Diagrammatic representation of protective system for water tanks

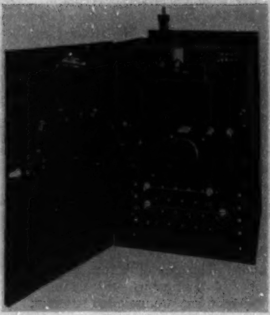

Figure 15. A 15,000-volt five-milliampere Rectox for metal separator

tremendous saving in bus copper, estimated to have been 80,000 pounds.

Figures 17 to 19 show the construction of one of these rectifier units, rated to deliver six volts, 5,000 amperes, or 12 volts 2,500 amperes direct current. Ventilating air is circulated through the unit in a closed system by a double blower. Heat is removed by a water-cooled heat exchanger. The rectifier power transformer is located between the two six-volt 2,500-ampere blocks of rectifier plates. In the top of the cubicle are three saturable reactors, used to control the output of the rectifier over a range of ten to one in both voltage and current. Contactors controlling the power to the rectifier and to the blower motor, type *AB* disconnecting breakers for the rectifier and for the control circuit, and temperature and fan-failure relays are all located at one end of the cubicle for accessibility.

An automatic control system is used by which the output of all the 24 rectifiers in one line is varied in proportion to the speed of the strip through the mill. In addition, each rectifier has individual control so it can be varied independently of the others to take care of changing requirements resulting from wear of the particular anodes which this rectifier may feed. This tinning line has been in successful operation since the middle of 1943.

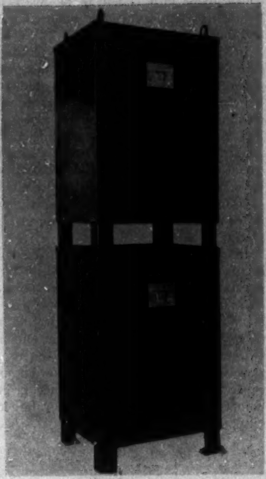

Figure 16. Stock models of six-volt 500-ampere Rectox electroplating rectifier

Figure 17. 12-volt 2500-ampere totally enclosed Rectox electroplating rectifier

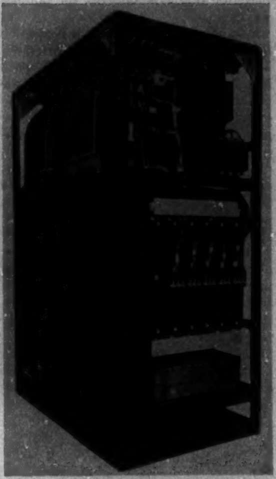

Figure 18. Totally enclosed rectifier showing arrangement of blowers, air duct, and control

Figure 19. Totally enclosed rectifier showing heat exchanger, Rectox plate assemblies, power transformer, and saturable reactors

The performance of one of these units is shown in Figure 20. Over-all efficiency is shown for the condition of fixed load resistance in part B. This is the condition existing when the line is slowed down and the rectifier output is varied to match the line speed. Efficiency at fixed current is shown in part D, this being the case when the anode wears away, requiring a higher voltage, but the same current. Efficiency at constant voltage is also shown in part A, although this is not a condition which occurs in the operation of the plating line. Power factor for the fixed resistance load is shown in part B, the power factor being determined mainly by the saturable reactors in the primary circuit. Other curves show the variation in output in terms of the variation in the saturating current of the reactor, and the variation in reactor voltage and transformer secondary voltage for the range of output voltage. Saturable-reactor control has the advantage of lending itself well to automatic operation and also of giving the same smooth variation in output as is obtained by rheostatic control of a generator.

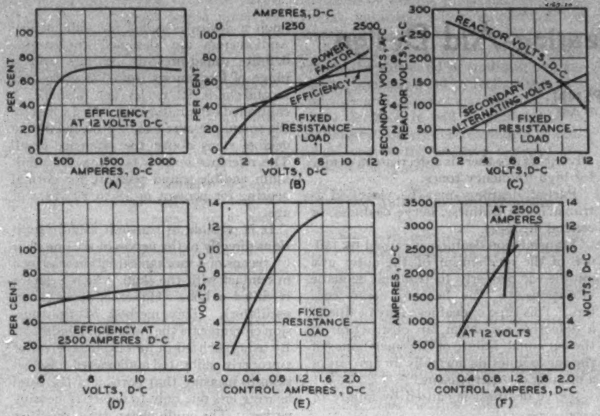

Figure 20. Performance curves of totally enclosed 12-volt 2,500-ampere rectifier

The circuits used in these applications are few in number and simple. Generally the ones involved will be found among those shown in Figure 21. It is not intended to discuss these here in any detail. Generally the single-phase and three-phase bridge circuits are used for electrochemical applications. Occasionally the full wave and the three-phase double wye may be used, their purpose being to obtain points of maximum efficiency at voltages half way between the maximum efficiency points of the bridge. Thus, if the bridge circuit gives maximum efficiency at 6, 12, 18 volts, and so on, the full-wave or the double-wye circuits will give maximum efficiency points at 3, 6, 9, 12, and so on.

In electrochemical applications, involving often continuous operation or operation over long periods, efficiency of the rectifier is of prime importance. This is true not only for full rating but for voltages below full rating. It will be realized that many six-volt plating rectifiers operate at only three or four volts. In cathodic-protection work a survey made several years ago of some 3,000 units in service showed that 75 per cent were operating at less than one-half their voltage rating, while only 2 per cent were at or near full rating. It is peculiar to this application that full output may be required when operation begins, but that after a period the power-output requirements reduce greatly due to polarization of the pipe line.

Hence the efficiency is important at partial voltages as well as at full voltage. In this respect, the copper-oxide rectifier is favored because of its low forward resistance, resulting in less reduction in efficiency for partial voltages than is the case with some other types of rectifiers. Figure 22 shows how efficiency varies with voltage output, load being constant.

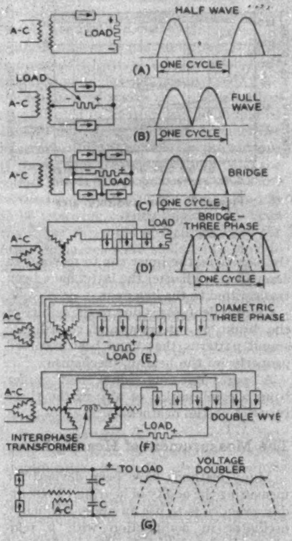

Figure 21. Circuits commonly used for metallic rectifiers

Long life is also a very desirable feature in electrochemical work. In this respect, the copper-oxide rectifier is supreme in its field, measuring its life in years rather than in hours.

Figure 23 is given to show how little actual effect aging has on performance. In November 1939 the first large electroplating Rectox was installed in which the plates had the plated-nickel contact coating. In Figure 23 the performance of this rectifier when installed is shown as curve *A*, while the performance after $4^{1}/_{2}$ years of service is shown on curve *B*. Study of these two curves shows there has been no discernible effect due to forward aging. The slight reduction at light loads is due to what aging has occurred in the back direction. Full load is obtained on exactly the same transformer tap now as when installed. Voltage regulation consequently shows no apparent change over the period. Prevailing ambient temperature for this rectifier is about 25 degrees centigrade.

This installation illustrates well the advantages of a plating rectifier. Here is a power plant that has operated over a $4^{1}/_{2}$-year period with the same full-load performance at all times, and with no expense to the user as yet for maintenance or repair of the rectifying units.

In Figure 24 is a typical life curve of the disk-type Rectox units used in operating d-c elevator brakes and control. This curve was taken on a large number of units in the field and shows a practical

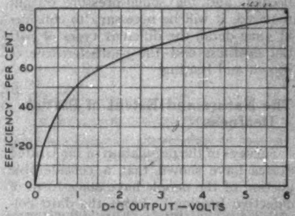

Figure 22. Variation of efficiency with load voltage at constant load for three-phase bridge Rectox

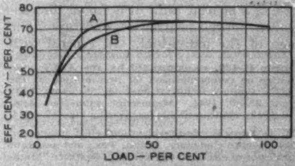

Figure 23. Efficiency curves showing effect of $4^{1}/_{2}$ years of service on a six-volt 2,000-ampere plating rectifier

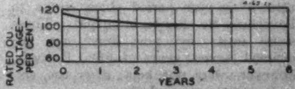

Figure 24. Field test on Rectox units supplying power for elevator control and brakes

Fundamentals of Hearing-Aid Design

W. D. PENN
ASSOCIATE AIEE

A HEARING AID is a miniature sound-amplifying system which is worn on the person in order to help overcome the handicap of impaired hearing. To be effective a hearing aid must be capable of amplifying those frequencies necessary for intelligibility of speech and reproducing them at a high-intensity level so as to make normal conversation audible to the person with impaired hearing. Modern hearing-aid amplifiers are of the vacuum-tube type, using small tubes designed specifically for them. A complete hearing aid consists of a microphone, a vacuum-tube amplifier, a receiver, and the necessary battery supply and connecting cords. Usually the microphone and amplifier unit are housed in a single case and the receiver and battery supply are connected to the amplifier by means of flexible cords. In order to determine the amount of amplification necessary, the type of frequency response needed, and the sound-pressure output required, it will be necessary to consider in some detail the different types of partial deafness and the characteristics of speech and hearing.

The Nature and Extent of Partial Deafness

Surveys of the population of the United States have shown that a considerable portion of the people have some form of defective hearing.[1] From the data collected it is possible to estimate the percentage of the population having a hearing loss greater than any particular designated value. Approximately four per cent have losses sufficient to require the use of a hearing aid. It is found that so-called normal hearing depends upon the age of the individual. In general with advancing age there is a decrease in acuity for high-frequency tones.

Partial deafness may be classified as transmission deafness, nerve deafness, or central deafness.

Transmission deafness is caused by failure of the mechanism of the outer and middle ear properly to transmit acoustic vibrations to the hearing nerve centers. This type of deafness may be due to a defective ear drum, an obstruction in the ear canal, failure of the small bones of the ear to function normally, or any one of a number of other reasons.

Nerve deafness is caused by failure of the nerves of the inner ear to respond to acoustic stimuli after the latter have been transmitted to the inner ear.

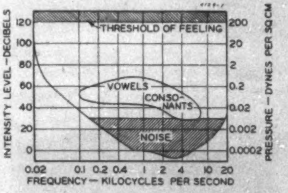

Figure 1. The auditory area

Central deafness is due to the failure of the brain to interpret and co-ordinate sound patterns that have been received properly by the hearing mechanism.

A particular individual may suffer from a combination of any of the three types of partial deafness.

The Measurement of Hearing Loss

Many methods have been devised for measuring the acuity of hearing. Present-day practice is to use an audio-frequency oscillator in conjunction with a telephone receiver which has been calibrated in terms of its acoustical sound output. Such a device is called an audiometer.[2] Audiometers are designed to provide definite sound-pressure outputs over a range of frequencies from 128 cycles per second to 8,192 cycles per second. A calibrated attenuator is provided whereby the sound pressure can be adjusted in five decibel steps above the reference level. The reference level is taken as the minimum audible sound pressure for normal hearing for persons of 18 to 23 years of age.

It is possible to transmit sound vibrations directly to the nerves of the inner ear by means of a mechanical vibrator placed in contact with the skull. Such a device is called a bone-conduction receiver (or oscillator). This method of transmission circumvents the outer and middle ear. By using such a unit for output receiver of a properly calibrated audiometer, it is possible to measure that portion of the total hearing loss due only to defective hearing nerves. The audiometer thus provides a means of measuring the minimum sound pressure required to evoke a sensation of hearing either by air conduction (telephone receiver) or by bone conduction. A curve showing the minimum audible sound pressure at different frequencies is called an audiogram. The audiometer measures the minimum sound pressure required for audibility and really is a measure of what is *not* heard rather than what is heard.

In estimating an individual's capacity for understanding speech from his audiogram, it is necessary to take into consideration the relative effects of different frequencies on the intelligibility of speech.[3]

The Auditory Area

The frequency range for normal hearing has been established as being from 20 to 20,000 cycles per second. The ear does not respond uniformly to sounds of different frequencies, so the minimum sound pressure required for audibility varies over the frequency range. The curve showing the minimum audible sound pressure is called the threshold of audibility curve. If at any frequency within the hearing range the sound pressure is increased progressively above the minimum audible pressure, an upper limit at which a sensation of pain occurs is reached. This upper limit is relatively

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stabilization of the output voltage with time. It is interesting to see that for the three-to-six-year period there seems to have been no change in output. These same units by now have an additional seven years of life. Records, however, were not kept after the six-year point was reached.

Laboratory units are still running which were started in 1926–28, so it is correct to say that there appears to be no limit to the life of a copper-oxide rectifier.

Conclusion

Electrochemical applications characteristically require high operating efficiency at all voltages up to full rating, operation free from maintenance, and long life. Some of these applications are among the oldest in which the copper-oxide rectifier has been used, and in which it has achieved great success. Familiarity with the rectifier is daily increasing, due to the much enlarged use of the past few years. It is believed that this ground will not be lost, but that the advantages of the copper-oxide rectifier will result in further expanding its use as these advantages become more generally recognized.

constant with frequency. The curve showing the maximum sound pressures is called the threshold of feeling.[4]

The area enclosed by the threshold of audibility and threshold of feeling curves, between the upper and lower limits of frequency is known as the auditory area. For a sound to be heard by an individual it must fall within the auditory area of that person. Usually there will be some particular intensity level in the auditory area of the partially deafened person which provides the most comfortable hearing. If a hearing aid is used, the intensity can be adjusted to the proper level by means of the volume control.

Characteristics of Speech

The prime purpose of a hearing aid is to amplify speech sounds so that maximum intelligibility results for the user of the aid.

The sounds of speech are composed of combinations of different tones spread throughout a range of frequencies from 100 cycles per second to 8,000 cycles per second. The vowel sounds embrace a range from approximately 100 to 2,500 cycles per second. Consonants consist mainly of high-frequency tones extending from 1,000 to 8,000 cycles per second. Each sound of speech is characterized by certain groups of frequencies and, unless these groups are present in the proper proportions, the speech will not be intelligible no matter how loud it may be.

Some frequency bands are more essential to the understanding of speech than others. The low frequencies of speech furnish the greater part of the acoustic energy. The high frequencies contribute the greater part of the intelligibility. Thus eliminating all frequencies below 500 cycles per second reduces the distinctness by only four per cent while reducing the acoustic energy by 62 per cent. On the other hand, eliminating all frequencies above 500 cycles per second reduces the distinctness or intelligibility by 93 per cent and removes only 38 per cent of the energy. There is but little loss of intelligibility if the range from 200 to 4,000 cycles per second is transmitted.[5]

Studies of the intensities of the various frequencies present in normal conversational speech show that they are distributed throughout the auditory area approximately as indicated in Figure 1.[6] The left hand ordinate of Figure 1 gives the intensity in decibels above a reference intensity of 10^{-16} watts per square centimeter. The reference pressure is thus 0.0002 dyne per square centimeter or 0.0002 bars. The right hand ordinate gives the pressure in bars. Examination of Figure 1 indicates that the principal voice frequencies vary in sound pressure from a minimum of 0.006 bar to a maximum of 0.6 bar. Expressed in decibels this would correspond to a volume range of 40 decibels. There will be some variation of volume range between individual speakers. For recognition of speech a volume range of 30 to 40 decibels is essential. If a hearing impairment causes the threshold of audibility to be so near the threshold of feeling that the auditory area does not encompass a range of 40 decibels, it will be impossible to understand speech even though it be amplified sufficiently to place it above the threshold of hearing.

Need for Selective Amplification

It has been pointed out that partial loss of hearing occurs in various forms. No two people with the same type of partial deafness will suffer exactly the same hearing loss at each frequency. In general, those suffering from nerve deafness hear low frequencies better. They hear sounds but do not understand the words because the high-frequency sounds are essential for intelligibility.

Unless the hearing loss is uniform throughout the frequency range it is necessary to utilize frequency selective amplification. Even though the hearing loss is uniform, it is advantageous in many instances to use the proper type of frequency selective amplification. If selective amplification is not used, then the frequencies which the individual has no difficulty in hearing will be amplified equally as well as those with which he has difficulty. The relative proportions of the various frequencies comprising the sound then will not be correct and some of the frequencies will become so loud as to be annoying. They might reach even the upper threshold of feeling before the other frequencies are sufficiently loud to indicate the characteristics of the speech sounds. Examination of Figure 1 shows why this occurs. If the areas representing the speech sounds are moved uniformly upward, the vowel sounds strike the feeling curve long before the consonants. Thus the vowel sounds will tend to mask the weaker consonant sounds which are essential for intelligibility if uniform amplification is used.

It appears then that for good intelligibility some form of frequency selective amplification must be used. Previous considerations of the characteristics of speech and hearing have shown that for intelligibility the high speech frequencies are necessary and that the low frequencies are not so important. From the manner in which the speech sounds are distributed in the auditory area, it is also apparent that when the hearing loss is severe it is necessary to attenuate the low frequencies in order to be able to bring the high-frequency sounds above threshold and still not have the low frequencies strike the threshold of feeling.

Attenuation of the low frequencies usually results in some increase in intelligibility even when the hearing loss is uniform. This is because in average room noise the intensities are higher for the low frequencies which thus produce a masking effect which can be lessened by reducing the intensity of these components.

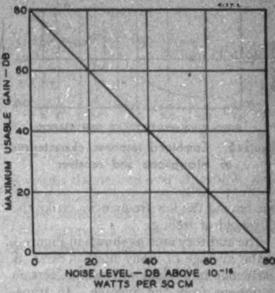

Figure 2. Maximum usable gain as a function of noise level

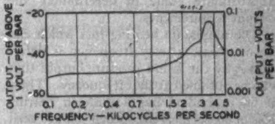

Figure 3. Response characteristic of hearing-aid microphone

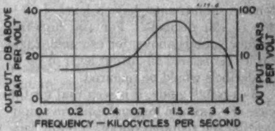

Figure 4. Response characteristic of hearing-aid receiver

From the above considerations it is reasonable to conclude that the type of selective amplification used should be such as to make possible attenuation of the low-frequency end of the spectrum. The control should be of the variable type since each case will be somewhat different.

Output Requirements

The maximum sound-pressure output which a hearing aid must be capable of producing can be determined from the chart of the auditory area. If uniform amplification is used, the speech level can be raised until the frequencies in the region from 500 to 1,500 cycles per second strike the upper threshold of 200 bars. The higher frequencies will then have pressures of from two to 20 bars. If the frequencies below 1,500 cycles per second are attenuated, it is possible to raise the output pressure at high frequencies with-

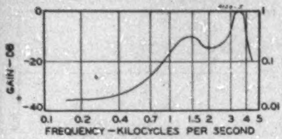

Figure 5. Combined response characteristic of microphone and receiver

out having the low frequencies strike the threshold of feeling.

The auditory area as shown in Figure 1 is the average of many normal ears. A particular individual will exhibit certain variations from the average. In transmission deafness it is probable that the threshold of feeling also is raised. The above figures nevertheless serve as an approximate guide in determining the output requirements.

The maximum hearing loss which can be overcome at any frequency is determined by the sound-pressure output that can be obtained at that frequency. Thus, if an individual has a uniform hearing loss of 60 decibels the speech sounds must be raised in intensity so that all of them are above a 60-decibel intensity level. Since the range between the loudest sound and the weakest is 40 decibels the loudest sounds must have a level of 60+40 = 100 decibels. For the high-frequency sounds above, for example, 2,000 cycles per second an output level of 60 to 80 decibels would be required.

As a rough guide, it is seen that for the case of a uniform loss and uniform amplification the required output level in decibels, L, must equal the hearing loss, HL, in decibels plus 40. That is

$$L = HL + 40 \qquad (1)$$

From equation 1 it would appear that with uniform amplification and uniform loss, the greatest hearing loss which could be overcome would be 80 decibels and in order to overcome a loss of this magnitude the hearing aid would have to be capable of supplying a pressure of 200 bars for frequencies less than 1,500 cycles per second and 20 bars for frequencies greater than 1,500 cycles per second.

If, as is often the case, the hearing loss increases with frequency then in order to bring the high-frequency components necessary for intelligibility above the impaired threshold and still not let the low frequencies reach the threshold of feeling, it is necessary to attenuate the low frequencies.

If the loss is greater for low frequencies the requirements are not so severe because this does not reduce greatly the intelligibility. Also, as mentioned previously, the type of hearing loss characterized by decreased acuity for low tones is usually due to transmission deafness and the upper threshold is raised so that uniform amplification can be employed with less possibility of striking the upper threshold of feeling. A recent tabulation of audiograms indicates that only 13 per cent of those tested had characteristics which showed a loss greater for low frequencies than for high frequencies. Increasing loss with frequency was shown by 43 per cent and 44 per cent had a uniform characteristic. Ten per cent of the total number showed a loss greater than 80 decibels.[7]

Nonlinear Distortion

If the relation between output sound pressure and input sound pressure is not linear, nonlinear or amplitude distortion occurs. Vacuum-tube amplifiers can be made to operate as substantially linear devices if certain limits of operation are not exceeded. However, no amplifier has an unlimited output capacity and so as the input signal is increased progressively a point finally is reached beyond which the output is not directly proportional to the input. Since the amplitudes are largest in the output stage, most of the distortion occurs there. As a result of the nonlinear relation between input and output, new frequencies are created in the output which are not present in the original signal.[8] Under such conditions the output sound pressure is not a faithful replica of the input signal. The extraneous frequencies present mask the original sounds thus reducing the distinctness of the speech. The maximum usable output obtainable from a given amplifier is limited by the amount of nonlinear distortion which can be tolerated.

Measurements of the amplitude distortion necessary to produce a detectable change in speech quality show that the amount of distortion which can be tolerated depends upon the band width of the amplifying system, the type of distortion (even or odd harmonics), and whether or not the distorted speech is compared directly with the undistorted speech.[9] With an upper cutoff frequency of 5,000 cycles per second it is found that from 12 to 17 per cent distortion can be introduced before a noticeable change in speech quality occurs. With an upper cutoff of 8,000 cycles per second the permissible distortion is reduced by approximately one half. For complex signals such as speech, the per cent distortion is defined as the amount of distortion produced by a pure tone having a peak value equal to the peak value of the complex wave. Satisfactory reproduction of speech will be obtained if the distortion is held below 15 per cent.

Gain Requirements

It is necessary to distinguish between output and gain. To be of practical use an instrument must develop the required output pressure at the ear when the source of sound is some distance away.

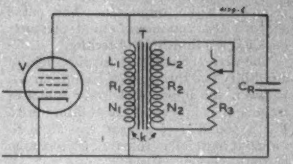

Figure 6. Circuit for controlling frequency response

The greater the amplification the less input signal the hearing aid will require to develop a given output pressure. At first thought it might appear that the maximum obtainable gain would be limited only by the number of stages of amplification which could be used in an instrument of wearable size. In practice there are two other factors which determine the maximum *usable* gain. The first of these is the surrounding noise level. Numerous measurements have shown that the extraneous noise level in the average quiet residence has an intensity level of about thirty decibels. In commercial buildings the noise level will be higher.[10] The effect of this extraneous noise level is to raise the threshold of hearing. A normal ear immersed in a noise level of 30 decibels would be unable to understand conversational speech unless the speech intensity level was greater than 30 decibels. In normal speech (at conversational distances) the loudest vowel sounds are 70 decibels above threshold and the weakest consonant sounds 30 decibels above threshold and thus they would be heard in spite of a noise level of 30 decibels. A hearing loss of 30 decibels would have the effect of eradicating the surrounding noise level but ordinary conversation still would be audible. If the source of sound were farther away, the desired sound would have a level less than the surrounding noise and no amount of amplification would help, since the noise and desired sound both would be amplified. The only solution is to have the desired signal louder than the background noise. Thus the auditor always must move close enough to the source of the desired sound to make its intensity level greater than that of the surrounding noise level. At conversational distances the speech sounds are located as shown in Figure 1. It is seen that the pressure of the loudest vowel sounds at the hearing-aid microphone will be 0.6 bar. To overcome the most severe case of deafness this pressure must be raised to 200 bars. This requires a pressure ratio or acoustical amplification of 200/0.6 = 333.3 which corresponds to approximately 50 decibels. The other weaker sounds, assuming uniform amplification, will be raised by 50 decibels also.

If the surrounding noise level is lowered, the distance between speaker and listener can be increased and the speech still will be above the noise level. The speech intensities arriving at the microphone will be less, and correspondingly greater gain will be required for the same sound-pressure output. On the other hand, if the noise level is increased, the distance between speaker and listener must be decreased in order to raise the speech sounds above the surrounding noise. The speech pressure incident upon the microphone now will be greater and less gain will be required for a given sound-pressure output. Thus the maximum usable gain is determined by the surrounding noise level. Under average conditions of 30 decibels noise level the speech intensities would be as shown in Figure 1 and the maximum usable gain would be 50 decibels. In order to hear in any given noise level either the speaker must raise his voice to an intensity above the noise level or the distance between the speaker and listener must be decreased. In any event, for a given noise level the speech must have a definite sound pressure if it is to be heard. Since the distance between threshold and feeling is approximately 120 decibels, the maximum usable gain in decibels is

$$\text{Maximum usable gain} = 120 - (\text{noise level} + 40) = 80 - \text{noise level} \quad (2)$$

All quantities in the above expression are in decibels. Figure 2 is a graph of equation 2.

Acoustic Feedback

It previously was stated that, in practice, two factors limit the maximum usable gain. One of these has been shown to be the surrounding noise level. The second limiting factor is acoustic feedback. A hearing aid produces a sound-pressure output many times greater than the input pressure. If a portion of the output is fed back into the input in proper phase and magnitude, oscillation results. When acoustic feedback or "squealing" occurs, the instrument ceases to be of use to the user. Hearing-aid receivers are of the insert type. They are held in place at the ear by means of a plastic mold, the tip of which extends into the ear canal. With an ideal ear mold all of the sound pressure would be delivered to the ear drum. In practice, due to the imperfect fit of the ear mold a certain amount of acoustic energy may escape and find its way back to the microphone. If at any frequency the attenuation in the return path from receiver to microphone is less than the acoustical gain of the instrument, oscillation occurs.

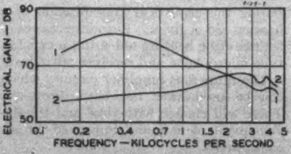

Figure 7. Frequency response of hearing-aid amplifier

When individually molded ear tips are used a gain of 60 to 70 decibels is ordinarily the maximum which can be had without feedback.

Computation of Acoustical Gain

The over-all acoustical amplification of a hearing aid will be affected by the characteristics of each of the component parts of the instrument.

It is customary to specify the output level of a microphone in terms of its open-circuit output voltage in decibels above a reference level of one volt per bar. Figure 3 shows the output of an uncased hearing-aid microphone of the crystal type as a function of frequency. When the microphone is enclosed in a case the response is altered somewhat by the acoustical properties of the case and this effect often can be used to advantage. The acoustical properties of the case are not included in the following analysis. Receiver output level can be expressed in decibels above a zero level of one bar per volt. The response characteristic of a crystal-type hearing-aid receiver is shown in Figure 4.

The response characteristics of the microphone and receiver may be combined into a single curve which then will show the ratio of output pressure to input pressure of the two units acting together. This ratio will be less than unity and, when expressed in decibels, is a negative number. The combined characteristic of the microphone of Figure 3 and receiver of Figure 4 is shown in Figure 5.

After the microphone and receiver have been selected, the electrical gain of the amplifier necessary for a specified acoustical gain can be determined.

In order to develop an expression for the over-all acoustical gain of microphone, amplifier, and receiver the following definitions are necessary.

Let

K_M = open-circuit output of microphone in volts per bar
K_R = output of receiver in bars per volt
$K_{MR} = K_M K_R$ = ratio of output pressure to input pressure of microphone and receiver with no amplification between
P_o = output pressure of receiver in bars
P_i = input pressure to microphone in bars
VA = voltage amplification of amplifier unit
$G = P_o/P_i$ = over-all acoustical gain
N_A = over-all acoustical gain in decibels

Then

$$P_o = (P_i)(K_M)(K_R)(VA) \quad (3)$$

and

$$G = P_o/P_i = (K_M)(K_R)(VA) = (K_{MR})(VA) \quad (4)$$

In decibels the gain is

$$N_A = 20 \log_{10} G = 20 \log_{10} (K_{MR}) + 20 \log_{10} (VA) \quad (5)$$

Letting

$N_{VA} = 20 \log_{10} VA$ and $N_K = 20 \log_{10} K_{MR}$ equation 5 becomes

$$N_A = N_{VA} + N_K \quad (6)$$

Knowing the desired over-all gain N_A and the combined characteristic N_K the necessary voltage amplification of the amplifier expressed in decibels is obtained from equation 6 as

$$N_A = N_{VA} - N_K \quad (7)$$

N_K is a negative quantity so the required electrical gain is greater than the desired acoustical gain by N_K decibels. The necessary electrical gain thus is established in terms of the desired acoustical gain and the characteristic of the microphone–receiver combination. For the microphone–receiver combination of Figure 5, $N_K = -33$ at 500 cycles per second. To achieve an acoustical gain of 50 decibels at 500 cycles per second would thus require a decibel voltage gain, N_{VA}, of 83.

Control of Frequency Response

It has been shown that in many instances selective amplification is desirable in a hearing aid. There are numerous methods for controlling the frequency response of an amplifier. Most of these are not applicable to hearing-aid use because of the additional circuit components necessary. In the hearing-aid industry it is common practice to secure frequency selectivity by using a number of interchangeable parts each having different response characteristics, or to manufacture a number of different models each with a different response. These methods have the disadvantage that a multiplicity of extra parts must be manufactured and stocked in order to meet the requirements of people with different types of deafness. A further disadvantage is that at the time of purchase it is often difficult to determine which combination eventually will give the most satisfactory results. It might seem that the best of a number of available instruments could be selected by a listening test. Experience has shown, however, that methods of fitting a hearing aid which comprise selecting one of a group of available characteristics

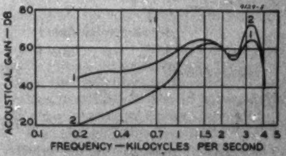

Figure 8. Acoustical gain of hearing aid

by listening test are not adequate. This is because the individual is accustomed to hearing sounds as they are rendered through his defective hearing mechanism. Thus, an instrument which makes sounds seem similar to those he is accustomed to hearing usually is chosen. As a result his impairment actually is exaggerated. It often is found that, after the user has become accustomed to an instrument, readjustment of the frequency response results in greater intelligibility. It is difficult to make such adjustments if they involve the exchange of an instrument or part of an instrument.

A novel method of controlling the frequency response of a hearing aid whereby the low frequencies can be attenuated while the high frequencies actually are accentuated now will be described.[11] This method is particularly suitable when used in conjunction with receivers which have a capacitative internal impedance, such as a crystal receiver. The method has the advantage that no interchangeable parts are necessary, and the variation in response is continuous throughout the range of operation.

The output stage of a hearing-aid amplifier usually is coupled to the receiver by means of a transformer or by means of a choke in order to obtain the largest possible output with a given "B" battery supply voltage. If a crystal receiver is used the internal capacity of the receiver and the inductance of the output choke form a parallel circuit with a resonant frequency determined by the LC product of the choke and receiver. The impedance of this parallel circuit is highest at resonance and maximum gain thus occurs at the resonant frequency. The frequency at which maximum gain occurs can be changed by varying the resonant frequency. The internal capacity of the receiver is fixed so the frequency of resonance must be changed by a variation of inductance. This could be done by tapping the coil but the number of resonant frequencies obtainable in this manner is limited.

Consider the circuit of Figure 6, in which the plate load of the output-tube V consists of the primary of transformer T in parallel with a receiver of internal capacity C_R. The secondary winding of T is connected to a variable resistance R_3. Considering the transformer by itself, the complex impedance looking into the primary is

$$Z_{12}=R_1+\frac{(M\omega)^2R_S}{R_S^2+(L_2\omega)^2}+ J\left\{L_1\omega-\frac{(M\omega)^2L_2\omega}{R_S^2+(L_2\omega)^2}\right\} \tag{8}$$

$$=R_{12}+JL_{12}\omega \tag{9}$$

In equation 8,

$$R_S=R_2+R_3 \tag{10}$$

The apparent resistance of the primary is thus R_{12} and apparent inductance is L_{12}.

Let

$$K=\frac{M}{\sqrt{L_1L_2}} \tag{11}$$

and

$$N=N_1/N_2\approx\sqrt{L_1/L_2} \tag{12}$$

Substitute equations 11 and 12 in 8 and obtain

$$Z_{12}=R_1+\frac{K^2N^2R_S}{1+(R_S/L_2\omega)^2}+ JL_1\omega\left\{1-\frac{K^2}{1+(R_S/L_2\omega)^2}\right\} \tag{13}$$

In terms of the coefficient of coupling, K, and the turn ratio, N, the apparent resistance looking into the primary is

$$R_{12}=R_1+\frac{K^2N^2R_S}{1+(R_S/L_2\omega)^2} \tag{14}$$

The apparent inductance is

$$L_{12}=L_1\left\{1-\frac{K^2}{1+(R_S/L_2\omega)^2}\right\} \tag{15}$$

Equation 15 is the basis of the selective circuit under discussion. If the secondary winding of T is short-circuited $R_S=R_2$. By suitable transformer design $R_2/L_2\omega$ can be made small compared to unity. Under these conditions equation 15 reduces to

$$L_{12}=L_1(1-K^2)=L_S \tag{16}$$

The quantity L_S is the leakage inductance of the transformer referred to the primary winding. The coefficient of coupling, K, depends upon the physical arrangement of the two coils L_1 and L_2 and upon the magnetic circuit on which they are wound. For coils wound on ferromagnetic cores, K can be made close to unity. L_S is then a few per cent of L_1.

With the secondary of T open-circuited ($R_3=\infty$) equation 15 becomes

$$L_{12}=L_1=\text{self-inductance of primary winding} \tag{17}$$

From equations 16 and 17 it is seen that any inductance value between L_1, the self-inductance of the primary winding, and L_S, the leakage inductance, can be obtained merely by varying the resistor R_3.

In the circuit of Figure 6 the primary of T is shunted by C_R, so the amplifier stage may be tuned to different resonant frequencies by means of a single variable resistor. Because of the resistance term, R_{12}, the resonance curves are very broad. The highest resonant frequency obtainable is

$$f_h=\frac{1}{2\pi\sqrt{L_SC_R}}=\frac{1}{2\pi\sqrt{L_1(1-K^2)C_R}} \tag{18}$$

The lowest resonant frequency obtainable is

$$f_l=\frac{1}{2\pi\sqrt{L_1C_R}} \tag{19}$$

The ratio of these two frequencies is

$$f_h/f_l=\frac{2\pi\sqrt{L_1C_R}}{2\pi\sqrt{L_1(1-K^2)C_R}}=\frac{1}{\sqrt{1-K^2}} \tag{20}$$

Thus any desired ratio of f_h/f_l can be obtained by providing the proper coefficient of coupling. The frequency response of the amplifier then can be controlled by varying R_3.

Performance of a Particular Hearing Aid

The curves of Figure 7 show the electrical response of a three-tube hearing-aid amplifier. The last stage of the amplifier uses the method previously described for varying the frequency response. Curve 1 is for R_3 open-circuited and curve 2 is for R_3 short-circuited. Any response characteristic lying between the two curves can be obtained by varying control R_3. Figure 8 shows the over-all acoustical amplification of a three-tube hearing aid having the electric gain shown in Figure 7. This instrument uses the microphone and receiver combination of Figure 5. Curve 1 of Figure 8 is obtained with the frequency control resistor, R_3, open-circuited. Curve 2 is obtained with the control resistor in the short-circuited position. The two curves indicate the maximum variation in response which is obtainable with this particular instrument. These curves were obtained using a 30-volt "B"-battery supply. At a frequency of 1,000 cycles per second an output pressure of 350 bars can be obtained with less than five per cent distortion when using a 30-volt supply.

Increasing the plate supply to 45 volts increases the gain by six decibels and provides approximately twice as much output pressure.

For methods of measuring the performance of hearing aids, reference 12 should be consulted.

Conclusion

This paper correlates the fundamental characteristics of speech and hearing which are of importance in the development and design of hearing aids. It is shown that often a particular type of selective amplification is beneficial. A novel method of obtaining this type of amplification is described. It is pointed out that certain factors external to the hearing aid determine the maximum performance obtainable. A method of computing the acoustical amplification of a hearing aid is given. Response curves of a three-tube hearing aid using a crystal-type microphone and receiver are shown.

The numerous amplifier circuits which can be used have not been discussed. These will change with time and the development of new and improved circuit components.

Future possibilities for improvement lie in the establishment of more accurate methods of fitting hearing aids to the individual's specific needs and in the use of two-channel or binaural hearing aids.

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Surface Heating by Induction

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Synopsis: Induction heating offers a method for heating of electrical conductors such as steel, brass, graphite, by exposing them to a varying magnetic field.

The application of induction heating to melting, forging, surface hardening, and other uses is becoming increasingly common, resulting in a mounting interest for a physical concept of the heating effect itself. This effect has been previously analyzed by means of partial differential equations whose solutions are Bessel functions.[1] It is the object of this paper to analyze the induction-heating effect by using concepts commonly applied to a-c engineering without involving differential equations. It was found that ordinary vector diagrams lead to quantitative expressions for the penetration of the magnetic field into the charge, for the distribution of eddy currents and generated heat, as well as for the impedance of the loaded inductor.

The analysis is limited to the heating of a body of cylindrical shape, surrounded by a uniform alternating magnetic field; this limitation is the same as commonly applied when partial differential equations are used for the analysis.

Another limitation is introduced by the assumption that the depth of penetration is very small compared to the radius of the charge. This assumption greatly simplifies the mathematical treatment without sacrifice to the physical concept. If the depth of penetration is five per cent of the radius of the charge, the expression for the generated heat in the load will deviate only 3.6 per cent from the evaluation by Bessel functions; for smaller depths of penetration the deviation approaches zero.

THE generation of heat within the iron core of a power transformer is termed the core loss, which is one form of induction heating. It is customary to divide the core loss into hysteresis and eddy-current loss. The hysteresis loss is a characteristic property of ferromagnetic materials, whereas the eddy-current loss is an I^2R loss due to current circulation in the material, resulting from electromotive forces induced by the varying induction. Replacing the iron of the core by a nonmagnetic metal will eliminate the hysteresis loss, but eddy currents will still be induced and heat generated. Hence, magnetic as well as nonmagnetic metals respond to induction heating.

When the frequency of the alternating magnetic flux is increased, hysteresis and eddy-current losses increase. The eddy-current loss, however, increases at a much greater rate than the hysteresis loss. At frequencies of the order of 10,000 cycles and more the eddy-current loss is predominant and the hysteresis loss is probably negligible.[2]

Among fields of application for induction heating are melting, forging, surface hardening, brazing, and soldering.[3,4,5] Induction heating is characterized by high concentration of heat per unit volume and close control of the transmitted heat. It is particularly suitable for surface hardening because the heat-producing eddy currents have a tendency to flow next to the surface of the charge. If a shaft is heated to hardening temperature at its surface zone only and is subsequently quenched, then only the surface zone hardens while its core remains soft. This distribution of hardness is often desirable. It gives the shaft a hard wear-resistant surface, whereas the core remains soft and thus avoids brittleness attendant to hardness.

The application of highly concentrated heat for surface hardening and forging shortens the heating time. This is important not only because of the saving of time in itself, but also because a short heat interval prevents or minimizes scale formation.

Although the heat is generated in the surface layer, the entire body of a charge can be heated if so desired. This is done simply by continued application of power, the heat being conducted thermally to the interior.

The Magnetic Field of the Air-Core Inductor

In order to heat a charge, it is placed inside a helical coil,[6,7] the inductor, the latter being energized from a high-frequency generator (Figure 1). Various types of high-frequency generators are on the market, such as rotary motor generators, electronic-tube oscillators, and spark-gap oscillators.[8,9,10] The frequencies for induction heating range from 60 cycles up to several megacycles. The generator energizes the inductor, thus producing an alternating magnetic flux. The latter induces electromotive forces within the charge, comparable to the induction of the voltage in the secondary of a transformer. The electromotive forces cause currents, the so-called eddy currents, to flow through the charge. The heating effect is the result of I^2R losses due to the eddy currents in combination with the electrical resistance of the charge.

It is assumed that the charge will be exposed to a uniform alternating magnetic field. Such a field is produced by a solenoid of infinite length, part of which is shown in Figure 2. The magnetic field intensity outside of the solenoid is zero; this is plausible, remembering that the return flux outside of the solenoid spreads over an infinitely large area, thus reducing the flux density to zero.[11]

Positive directions were assigned to the magnetic field intensity and the current. They are shown by arrows parallel to the axis of the inductor and by the head and tail symbols, respectively, indicating a right-hand system. For alternating magnetic intensities and currents the arrows show the direction of the instantaneous values, provided the latter appear positive in their respective vector diagrams.

For an infinitely long inductor and charge, which will be assumed in all of the following considerations, the field

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distribution is the same for any cross section perpendicular to its axis. It is, therefore, permissible to analyze only a limited height of inductor and charge. The magnetic-field and eddy-current distribution thus obtained will be representative for the entire height of inductor and charge. The height of the section under consideration will be limited arbitrarily to one centimeter.

The magnetic-field intensity, henceforth abbreviated magnetic intensity, follows from (see Appendix VII for symbols):

$$H=0.4\pi In \qquad (1)$$

If the current I is alternating, the magnetic intensity H is also alternating and in time phase with the current I. Equation 1 can be rewritten

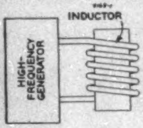


Figure 1. General arrangement of inductor and charge

$$\mathbf{H}=0.4\pi \mathbf{I}n \qquad (2)$$

The boldface letters $\mathbf{H}$ and $\mathbf{I}$ designate vector quantities. The letters H and I signify the absolute values of the vectors. The instantaneous values h and i are obtained by projecting the vectors $\mathbf{H}$ and $\mathbf{I}$, which are standing still, on the time base T (see Figure 3) which rotates clockwise with an angular velocity:

$$\omega=2\pi f \qquad (3)$$

The location of the time axis T for the time $t=0$ is coinciding with vectors $\mathbf{I}$ and $\mathbf{H}$. The angle α between $\mathbf{I}$ and T is

$$\alpha=\omega t \text{ in radians} \qquad (4)$$

where t is the time lapse from coincidence of T with $\mathbf{H}$ until T reaches the position shown in Figure 3. The quantities are positive for $-\frac{\pi}{2}<\alpha<\frac{\pi}{2}$, they are negative for $\frac{\pi}{2}<\alpha<\frac{3\pi}{2}$.

Screening Effect

Suppose the inductor (Figure 4) is energized by a constant alternating current, an alternating field intensity H_0 will result, which is constant over the whole cross section of the infinitely long inductor. A thin-walled tubular metallic charge A is now inserted coaxially. The variable magnetic flux surrounded by the charge induces voltages and currents in the charge in the same manner as in the secondary of a transformer, consisting of one turn which is short-circuited.

It is convenient to assume the positive directions of both inductor and charge current in the same right-hand-system direction, as indicated by the symbols in Figure 4.

Similar to the current I in the inductor, the current I_A circulating circumferentially in the infinitely long charge produces a magnetic intensity ΔH_0 which is constant throughout the space surrounded by the charge and which is zero for any point outside. The resulting field intensity at any instant can be regarded as a result of superposing the magnetic intensities caused by the currents in inductor and charge. The magnetic field intensity H_1 in the space surrounded by the charge is

$$\mathbf{H}_1=\mathbf{H}_0+\Delta \mathbf{H}_0 \qquad (5)$$

where H_0 is the magnetic intensity produced by the inductor current alone and ΔH_0 the magnetic intensity produced by the charge current alone. In analogy to equation 1 and for $n=1$

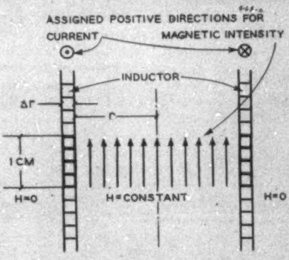


Figure 2. Magnetic intensities of infinitely long empty air-core inductor

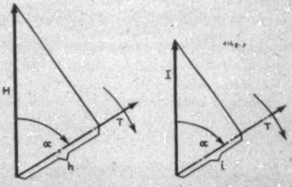


Figure 3. Vector diagrams in general

$$\Delta \mathbf{H}_0=0.4\pi \mathbf{I}_A \qquad (6)$$

The induction B_1 in the space surrounded by the charge is

$$\mathbf{B}_1=\mu \mathbf{H}_1 \qquad (7)$$

and the magnetic flux Φ_1 surrounded by the charge is

$$\Phi_1=B_1a^2\pi=\mu H_1a^2\pi \qquad (8)$$

$\mathbf{\Phi}_1$ and $\mathbf{H}_1$ are parallel vectors. The electromotive force $\mathbf{E}_A$ generated in the charge A by flux $\mathbf{\Phi}_1$ is lagging 90 degrees behind it (Figure 5). Overcoming the resistance of the current path, $\mathbf{E}_A$ causes the flow of current $\mathbf{I}_A$, which is in phase with $\mathbf{E}_A$. $\Delta\mathbf{H}_0$ is parallel to $\mathbf{I}_A$. $\mathbf{H}_0$ follows from equation 5 as $0P$. The flux $\mathbf{\Phi}_0$ between inductor and charge is in phase with $\mathbf{H}_0$.

An inspection of the vector diagram of Figure 5 reveals:

1. The magnetic intensity H_0 outside the charge is reduced in intensity to H_1 as it penetrates the charge. This effect which is due to the circulation of eddy currents in the charge is often called screening effect.

2. The currents in the charge cause a time lag between the magnetic intensities, resulting in a lag of the flux $\mathbf{\Phi}_1$ surrounded by the charge, with respect to the flux $\mathbf{\Phi}_0$ outside of the charge.

Solid Cylindrical Charge

GENERAL CONSIDERATIONS

A solid charge can be considered made up by a large number of thin-walled tubular sleeves which are telescoped into each other; the eddy-current heating of a solid charge will be approached by considering the current distribution in the individual thin-walled sleeves, reducing the thickness of the latter toward zero.

Judging from the case of a single thin-walled sleeve, one is led to expect that the magnetic intensity $\mathbf{H}_0$ will be reduced a certain percentage while penetrating the sleeve S_I (Figure 6) and that it will emerge from sleeve S_I as the intensity $\mathbf{H}_1$; it can also be expected that the emerging field intensity $\mathbf{H}_1$ will lag behind the entering intensity $\mathbf{H}_0$. The vectors of the magnetic intensities are shown in larger scale in Figure 7. The

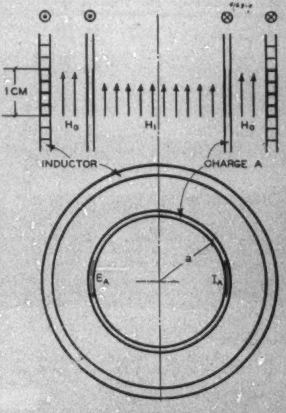


Figure 4. Field distribution in inductor containing a tubular charge of small wall thickness

angle $01P$ between $\Delta \boldsymbol{H}_0$ and $\boldsymbol{H}_1$ (Figure 7), however, cannot be expected to remain 90 degrees as in the case of Figure 5, because the current I_1 flowing in sleeve S_1 is no longer determined by the electromagnetic action of sleeve S_1 only, but also by the electromagnetic action of all the other sleeves S_{II}, S_{III} . . . . . inside of S_1. The current I_1 in S_1 is a function of the total flux surrounded by S_1, which in turn is a function of the different currents in all the sleeves. All that can be expected is that the magnetic intensity $\boldsymbol{H}_0$, by penetrating sleeve S_1 of the thickness Δr, will emerge as the intensity $\boldsymbol{H}_1$, lagging $\Delta\alpha$ behind $\boldsymbol{H}_0$, and being reduced in magnitude by a definite percentage of its entering value. The magnetic intensity $\boldsymbol{H}_1$ then enters sleeve S_{II}. If the wall thickness is the same for S_1 and S_{II}, it can be assumed that the emerging magnetic intensity $\boldsymbol{H}_2$ will lag by the same angle $\Delta\alpha$ behind $\boldsymbol{H}_1$ as $\boldsymbol{H}_1$ was lagging behind $\boldsymbol{H}_0$ in sleeve S_1. The ratio of reduction of H_1 to H_2 can be assumed to be equal to H_0 to H_1. These assumptions are justified by leading to interdependent systems of magnetic intensities and fluxes, electromotive forces, and currents, which fulfill all physical requirements.

VECTOR DIAGRAM OF MAGNETIC INTENSITIES

A vector diagram embodying the foregoing assumptions is shown in Figure 7. Let

$$H_1/H_0 = H_2/H_1 = \ldots\ldots H_m/H_{m-1} = q = \text{constant} \qquad (9)$$

$$\sphericalangle 0P1 = 1P2 = 2P3 = \ldots\ldots = \Delta\alpha = \text{constant}$$

It follows that the triangles $P01$, $P12$, $P23$, ... are similar (two homologous sides and one included angle $\Delta\alpha$). These geometric relations are mathematically expressed by

$$H_m = H_0\epsilon^{-(m\Delta\alpha)\cot\beta} \qquad \alpha \text{ in radians} \qquad (10)$$

as shown in Appendix I. If the phase angle of $\boldsymbol{H}_m$ with respect to $\boldsymbol{H}_0$ is α, then

$$\alpha = m\,\Delta\alpha \qquad (11)$$

and

$$H_m = H_0\epsilon^{-\alpha\cot\beta} \qquad \alpha \text{ in radians} \qquad (12)$$

indicating a logarithmic spiral as locus for the magnetic intensity.

VECTOR DIAGRAM OF MAGNETIC FLUXES

The flux $\boldsymbol{\Phi}_I$ contained within the body of sleeve S_1 is

$$\Phi_I = \boldsymbol{B}_1 \text{ area} = \mu \boldsymbol{H}_I[(\Delta r/2)(a - \Delta r/2)\pi] \qquad (13a)$$

likewise

$$\Phi_M = \mu \boldsymbol{H}_M\left[(\Delta r/2)\left(a - \frac{2m-1}{2}\Delta r\right)\pi\right] \qquad (13m)$$

Subscripts I and M refer to the quantities existing in the middle of the body of the first and mth sleeve, respectively, whereas subscripts 1 and m refer to the quantities at the lateral inner surface of the first and mth sleeve, respectively.

It can be seen from Figure 7 that the magnetic intensities and thus the inductions decline very rapidly as the magnetic field penetrates into the charge. The variation of the areas over which the fluxes are spread can be neglected if the magnetic intensity rapidly decreases as it penetrates the charge. Thus

$$\Phi_I = H_{I}\mu\,\Delta r 2a\pi \qquad (14a)$$

$$\Phi_{II} = H_{II}\mu\,\Delta r 2a\pi \qquad (14b)$$

$$\Phi_M = H_M\mu\,\Delta r 2a\pi \qquad (14m)$$

Adding the fluxes Φ_I, Φ_{II}, Φ_{III} . . . . . vectorially (by using equations 14a, 14b, and 14m in combination with Figure 7), the vector sum follows a logarithmic spiral which is similar to the locus for the magnetic intensity as proved in Appendix II. The spiral for the fluxes is shown in Figure 8.

The total magnetic flux Φ inside of the charge is the vector sum of all individual fluxes Φ_I, Φ_{II}, Φ_{III}. . . . . For a sleeve of infinitesimal thickness the angle $\Delta\alpha = 0$ and the phase angle between total flux Φ inside the entire charge and the magnetic intensity $\boldsymbol{H}_0$ between inductor and charge is

$$\beta = 45 \text{ degrees} \qquad (21)$$

Since inductor current $\boldsymbol{I}$ and $\boldsymbol{H}_0$ are in phase, the angle between $\boldsymbol{I}$ and Φ is also 45 degrees (Figure 9).

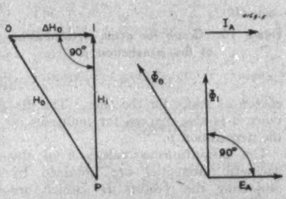


Figure 5. Vector diagram for inductor with tubular charge of small wall thickness

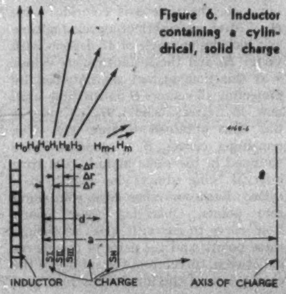


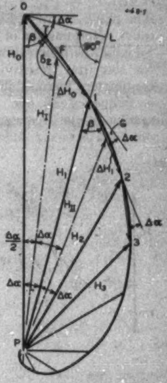
Figure 6. Inductor containing a cylindrical, solid charge

POWER FACTOR OF CHARGE

The total flux Φ induces a voltage $\boldsymbol{E}$ in the inductor which is 90 degrees lagging behind Φ and 45 additional degrees behind the inductor current $\boldsymbol{I}$. Voltage $\boldsymbol{E}$ consists of two components $\boldsymbol{E}_R$ and $\boldsymbol{E}_X$ of equal magnitude. $\boldsymbol{E}_R$ is in counterphase with $\boldsymbol{I}$ and reflects the watt power consumed by the charge. $\boldsymbol{E}_X$ is lagging 90 degrees behind the current and indicates that the charge consumes inductive power as well. Due to the equality in magnitude of $\boldsymbol{E}_R$ and $\boldsymbol{E}_X$, the load consumes as much watt power as wattless inductive power. Therefore,

$$\text{Power factor of charge} = \cos 45 \text{ degrees} = 0.707 \text{ inductive} \qquad (22)$$

Figure 7. Vector diagram of magnetic intensities



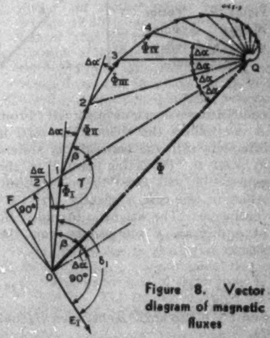


Figure 8. Vector diagram of magnetic fluxes

PENETRATION OF THE MAGNETIC FIELD

The total magnetic flux Φ within the boundaries of the charge is evaluated in Appendix III:

$$\Phi = B_0(2a\pi)p \qquad (30)$$

where B_0 is the induction in the space between inductor and charge. If the induction B_0 would penetrate into the charge to a depth of p centimeters, without being attenuated, the magnitude of the flux thus obtained would be equal to the magnitude of the actual flux Φ. For this reason the factor p is often called equivalent depth of the magnetic-field penetration, or shortly penetration. It follows from Appendix III:

$$p=\frac{1}{2\pi}\sqrt{\frac{\rho 10^9}{2\mu f}} \tag{31}$$

The graph shown in Figure 10 serves for quick determination of the penetration p. Suppose the penetration should be found for soft iron (resistivity $\rho=10^{-5}$ ohm-centimeter, permeability $\mu=1{,}000$) at a frequency of one kilocycle. Intersection of the frequency and resistivity co-ordinates occurs at point A. From A one follows the oblique line until it intersects with the permeability system at point B. Projecting point B on the penetration axis gives the penetration, which in this case is slightly over 10^{-2} centimeter. The magnetic intensity H_d at a distance d centimeters from the surface is

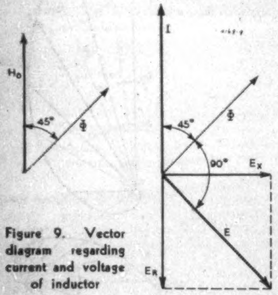


Figure 9. Vector diagram regarding current and voltage of inductor

$$H_d=H_0\epsilon^{-\frac{d}{p\sqrt{2}}} \tag{35}$$

and the phase angle α_d between H_d and H_0

$$\alpha_d=d/(p\sqrt{2}) \text{ in radians} \tag{36}$$

FIELD PROPAGATION INSIDE THE CHARGE

Returning to the vector diagram of Figure 7, the various vectors $\boldsymbol{H}_0$, $\boldsymbol{H}_1$, $\boldsymbol{H}_2$ represent the magnetic intensities at definite distances from the surface of the charge. These distances d follow from equation 36. In the vector diagram of Figure 7 the angle $\Delta\alpha$ was made 15 degrees, which is identical with $\pi/12$ radians. The distance at which vector $\boldsymbol{H}_1$ represents the magnetic intensity can be obtained from equation 36:

$$d=\alpha_d p\sqrt{2}=(\pi/12)p\sqrt{2} \text{ in centimeters}$$

Conversely, the magnitude and phase of the magnetic field for any given distance d from the surface can be obtained by computing α_d from equation 36, and subtending α_d from $\boldsymbol{H}_0$ to intersect the logarithmic spiral.

Using the vector diagram of Figure 7, a graph of the instantaneous distribution of magnetic intensities can be plotted (Figure 11). The plane of the graph can be considered as a partial lateral section through the cylindrical charge. The graph is limited on the left-hand side by the surface contour of the charge. The axis of the charge is parallel to the contour off to the right. The abscissa axis can be viewed as one radius of the charge. In order to generalize the graph, relative values $d/(p\sqrt{2})$ and h/H_0 were chosen as scales for the axes. The distance d is also marked for multiples of the penetration p.

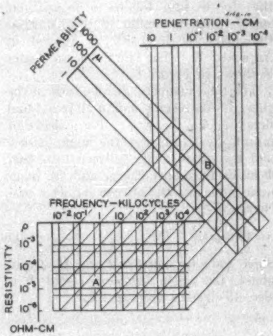


Figure 10. Graph for quick determination of the penetration p

The instantaneous values h of the magnetic intensities are obtained by projecting the vectors $\boldsymbol{H}$ (which are standing still) on a common time axis T. The time axis rotates clockwise and makes one revolution during each cycle of the high-frequency current (equation 3). For $t=0$, the time axis coincides with $\boldsymbol{H}_0$. After one eighth of a cycle the time axis includes an angle of 45 degrees with vector $\boldsymbol{H}_0$ and the instantaneous values h at this time instant are obtained by projecting all vectors $\boldsymbol{H}$ on the time axis, now 45 degrees behind $\boldsymbol{H}_0$. Curve A was thus obtained (Figure 11). The remaining curves B, C, and D were obtained in the same manner, the time interval being always one eighth of a cycle. Each curve has crest points and zero points. Observing these points from curve to curve, it looks as though these points, and the parts of the curves in between, travel from the surface of the charge toward the interior, as time goes on. It will also be observed that the height of the wave decreases as it penetrates the charge. It is therefore possible to view the field distribution inside of the charge as the result of an electromagnetic wave entering the charge at its surface and being attenuated as it proceeds.

DENSITY OF EDDY CURRENTS

The current density K_0 in the surface layer follows from Appendix IV.

$$K_0=In/p \text{ amperes per square centimeter} \tag{37}$$

In general, the current density depends on the flux with which the current filament is interlinked and the resistance of the path; the latter is assumed constant. Hence the current densities can be expressed by the same exponential function as the interlinking fluxes and the magnetic intensities, respectively (equations 12 and 21). The current density K_d at the distance d from the surface

$$K_d=K_0\epsilon^{-\frac{d}{p\sqrt{2}}}=(In/p)\epsilon^{-\frac{d}{p\sqrt{2}}} \tag{37a}$$

The generation of heat per unit volume at any depth d is:

$$w_d={}^1/_2K_d{}^2\rho={}^1/_2(In/p)^2\rho\epsilon^{-\frac{d\sqrt{2}}{p}} \text{ watts per cubic centimeter} \tag{37b}$$

Plotting the function $\epsilon^{-\frac{d\sqrt{2}}{p}}$ in Figure 12 discloses the rapid reduction of heat generation with increasing distance from the surface. The concentration of the eddy currents and hence the generation of heat next to the surface of the charge is called skin effect. The heat generated in a zone reaching to a depth of $3p$ is 98.5 per cent of the total generated heat.

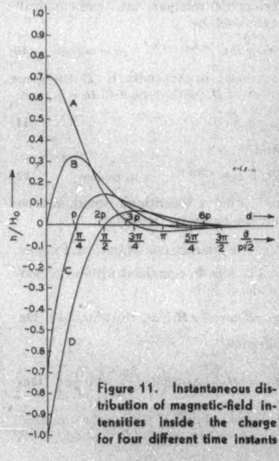


Figure 11. Instantaneous distribution of magnetic-field intensities inside the charge for four different time instants

Since the penetration p depends on the frequency of the inductor current (equation 31) one can vary the thickness of the heated zone by controlling the frequency. The case thickness can be reduced by increasing the frequency, conversely lowering the frequency will increase the depth of the heated zone as it is desirable in forging and melting applications.

POWER TRANSFER AND IMPEDANCE OF LOADED INDUCTOR

The power P delivered to the charge per centimeter axial length is shown in Appendix V.

$$P=4\pi^2 a\,(I_{eff}n)^2\sqrt{\mu f\rho\,10^9}\,10^{-9}\text{ watts per centimeter axial length of charge}\qquad(38)$$

All other conditions remaining the same, increasing the frequency increases the rate of heating. A material with greater resistivity will generate more heat, provided the depth of penetration is much smaller than the radius of the charge (see synopsis). Moreover, a material with a positive temperature-resistivity coefficient, will generate heat in increasing measure as it warms up.

Due to permeability, ferromagnetic materials will heat more rapidly than nonferrous materials of the same resistivity. However, when the critical temperature is reached ferromagnetism disappears and the permeability drops to unity. Thus, the heating of ferromagnetic materials is greatly reduced when the critical temperature is obtained.

As previously pointed out, the charge consumes watt power and a numerically equal amount of wattless inductive power. The inductor presents itself with a certain impedance, which is called the referred impedance Z_a of the load (charge); in addition the inductor will have a self-impedance Z_b consisting of the high-frequency impedance of its own winding and a reactance due to the magnetic field in the interspace between inductor and charge.

The above considerations lead to the expression for the referred resistance R_a of the load, as shown in Appendix VI.

$$R_a=4\pi^2 an^2\sqrt{\mu\rho f\,10^9}\,10^{-9}\text{ ohms per centimeter axial length of charge}\qquad(39)$$

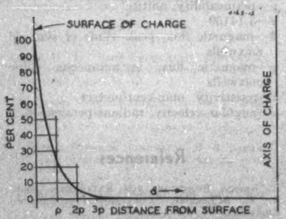


Figure 12. Relative heat generation as a function of the distance d from the surface

Since watt and wattless power are numerically equal, the referred reactance

$$|X_a|=R_a$$

and the referred impedance

$$Z_a=R_a+jX_a=R_a(1+j)$$

The high-frequency resistance R_b of the inductor can be approximated by using equation 39. For a water-cooled copper inductor the resistance R_b is according to Appendix VI:

$$R_b=1.8\,b\sqrt{f}\,n^2(c_1/c_2)10^{-6}\text{ ohms per centimeter axial length of charge}\qquad(40)$$

The losses in the inductor increase with its radius b. In order to obtain high efficiency of power transfer, the radius of the inductor should not be larger than necessary. Hence, the clearance between inductor and charge should be kept to a minimum.

Since watt and wattless power are numerically equal, the referred reactance of the inductor due to the field within the boundaries of the conductors is:

$$|X_b|=R_b$$

Hence the impedance

$$Z_b=R_b+jX_b=R_b(1+j)$$

The reactance X_0 due to the magnetic field in the interspace between inductor and charge is shown in Appendix VI.

$$X_0=8\pi^2 f\,(b^2-a^2)n^2\,10^{-9}\text{ ohms per centimeter axial length of charge}\qquad(41)$$

The total impedance Z of the loaded inductor is therefore:

$$Z=(R_a+R_b)+j(R_a+R_b+X_0)\text{ ohms per centimeter axial length of charge}\qquad(42)$$

Appendix I

The ratio of two consecutive magnetic intensities from equation 10 is

$$H_m=H_0\epsilon^{-m\Delta\alpha\cot\beta}$$

$$H_{m-1}=H_0\epsilon^{-(m-1)\Delta\alpha\cot\beta}$$

Therefore:

$$H_m/H_{m-1}=\epsilon^{-\Delta\alpha\cot\beta}=q\qquad(10a)$$

which is constant for positive, integral m.

$$q=H_1/H_0=(\sin\beta)/\sin(\beta+\Delta\alpha)$$

$$=1-\frac{\sin(\beta+\Delta\alpha)-\sin\beta}{\sin(\beta+\Delta\alpha)}$$

$$=1-\frac{2\cos(\beta+\Delta\alpha/2)\sin(\Delta\alpha/2)}{\sin(\beta+\Delta\alpha)}$$

For sleeves of infinitesimal wall thickness

$$\sin\Delta\alpha/2=\Delta\alpha/2\qquad(10b)$$

and

$$\beta+\Delta\alpha/2=\beta+\Delta\alpha=\beta\qquad(10c)$$

hence

$$q=1-\Delta\alpha\cot\beta\qquad(10d)$$

The same result is obtained by using equations 10 and 10a:

$$\epsilon^{-\Delta\alpha\cot\beta}=1-\Delta\alpha\cot\beta\text{ for }\Delta\alpha\cot\beta\ll1$$

Appendix II

Vector 01 representing Φ_I (Figure 8) is parallel to $\boldsymbol{H}_I$ of Figure 7 and determined by equation 14a. Vector 12 is parallel to $\boldsymbol{H}_{II}$ and determined by equation 14b. Hence

$$\Phi_{II}/\Phi_I=H_{II}/H_I\qquad(15)$$

$\boldsymbol{H}_I$ connects midpoint F of 01 (Figure 7) to P; $\boldsymbol{H}_{II}$ was obtained likewise. Triangles $0PF$ and $1PG$ are similar (two homologous sides and one included angle β) and hence:

$$H_{II}/H_I=H_1/H_0\qquad(16)$$

From equations 9, 15, and 16:

$$\Phi_{II}=\Phi_I H_{II}/H_I=\Phi_I H_1/H_0=\Phi_I q\qquad(17)$$

Point Q (Figure 8) was obtained by subtending the angle β from Φ_I and Φ_{II} (Figure 8).

$$\gamma=180°-\Delta\alpha-\beta$$

$$\sphericalangle 1Q0=180-\beta-\gamma=180-\beta-180+\Delta\alpha+\beta=\Delta\alpha°\qquad(18)$$

Triangles $01Q$, $12Q$, $01P$, and $12P$ are similar.[*] If another similar triangle $23Q$ is based on $2Q$, it can be shown in the same manner that 23 represents vector Φ_{III}. The sum of the first three individual fluxes Φ_I, Φ_{II}, Φ_{III} is $Q3$. The remaining individual fluxes and their sum can be determined by a repetition of the same procedure. The triangles become progressively smaller and finally degenerate in point Q. Line $0Q$ therefore represents the vector sum of all fluxes, and is denoted Φ. It is the total magnetic flux within the boundaries of the charge. Due to the similarity of all triangles involved, curve $01234\ldots P$ (Figure 7) is similar to $01234\ldots Q$ (Figure 8).

The electromotive force $\boldsymbol{E}_I$ induced in sleeve S_I is due to the action of all the fluxes Φ_{II}, Φ_{III} . . . . ., surrounded by sleeve S_I. The vector sum of these fluxes is $1Q$ (Figure 8). $\boldsymbol{E}_I$ is lagging 90 degrees behind $1Q$. Angle δ_1 between $\boldsymbol{E}_I$ and Φ_I is

$$\delta_1=\beta+\Delta\alpha+90°\qquad(19)$$

Angle δ_2 between $\Delta\boldsymbol{H}_0$ and $\boldsymbol{H}_I$ (Figure 7) can be expressed

$$\delta_2=180°-\beta-\Delta\alpha/2\qquad(20)$$

$\Delta\boldsymbol{H}_0$ is in phase with $\boldsymbol{I}_I$ (equation 6) and $\boldsymbol{H}_I$ is in phase with Φ_I, equation 14a, so that δ_2 is also the angle between Φ_I and $\boldsymbol{I}$. Remembering that $\boldsymbol{I}_I$ and $\boldsymbol{E}_I$ are in phase, angle δ_2 is the angle between Φ_I and $\boldsymbol{E}_I$ as well. This angle has been previously denoted with δ_1. Therefore from equations 19 and 20:

$$\delta_1=\delta_2$$

$$\beta+\Delta\alpha+90°=180-\beta-\Delta\alpha/2$$

$$\beta=45°-3\Delta\alpha/4$$

For infinitesimal thickness of the sleeve, $\Delta\alpha$ disappears and

$$\beta = 45 \text{ degrees} \tag{21}$$

Appendix III

The electromotive force E_0 in the surface zone of the charge is a function of the total flux Φ:

$$e_0 = -\frac{d\varphi}{dt}\,10^{-8}$$

hence the crest value

$$E_0 = 2\pi f\Phi 10^{-8} \tag{23}$$

The current I_0 in the surface layer

$$I_0 = E_0/R_0 \tag{24}$$

where R_0 is the resistance of the surface layer per centimeter axial length of the charge

$$R_0 = \frac{2a\pi}{\Delta r\cdot 1\ cm}\rho \tag{25}$$

From equations 23, 24, and 25,

$$I_0 = 2\pi f\Phi 10^{-8}\frac{\Delta r}{2a\pi\rho} = \frac{\Delta r}{a}\frac{f}{\rho}\,\Phi 10^{-8} \tag{26}$$

$$\Delta H_0 = 0.4\pi I_0 = 4\pi(f/\rho)(\Delta r/a)\Phi 10^{-9} \tag{27}$$

Because of the similarity of triangles $0P1$ (Figure 7) and $0Q1$ (Figure 8)

$$\Phi/\Phi_1 = H_0/\Delta H_0 \tag{28}$$

For infinitesimal sleeve thickness $H_0 = H_1$, moreover, substituting for H_1 from equation 14a and for ΔH_0 from equation 27, one obtains from equation 28

$$\frac{\Phi}{\Phi_1} = \frac{\Phi_1/\mu\Delta r 2a\pi}{4\pi(f/\rho)(\Delta r/a)\Phi 10^{-9}} = \frac{1}{\Delta r}\sqrt{\frac{1}{8\pi^3}\frac{\rho 10^9}{\mu f}} \tag{29}$$

Substituting for Φ_1 from equation 14a and for $B_1 = B_0$:

$$\Phi = B_0 2a\pi\left(\frac{1}{2\pi}\sqrt{\frac{\rho 10^9}{2\mu f}}\right) = B_0 2a\pi p \tag{30}$$

where

$$p = \frac{1}{2\pi}\sqrt{\frac{\rho 10^9}{2\mu f}} \tag{31}$$

Figure 8 reveals that

$$\Phi_1/\Phi = (\sin\Delta\alpha)/\sin(\beta + \Delta\alpha)$$

Considering equations 10b, 10c, and 21,

$$\Phi_1/\Phi = \Delta\alpha\sqrt{2}$$

which combined with equation 29, gives

$$\Phi_1/\Phi = \Delta\alpha\sqrt{2} = \Delta r/p \tag{32}$$

The magnetic field by penetrating sleeve by sleeve accumulates for each sleeve thickness Δr a phase displacement of $\Delta\alpha$. Penetrating m sleeves

$$d = m\Delta r \tag{33}$$

The phase angle α follows from equation 11

$$\alpha = m\Delta\alpha$$

Using equations 11 and 33 in equation 32,

$$\Delta r/\Delta\alpha = d/\alpha = p\sqrt{2} \tag{34}$$

Substituting equation 34 in equations 12 and 21 and using the subscript d instead of m, indicating the magnetic intensity at the distance d from the surface,

$$H_d = H_0\epsilon^{-\frac{d}{p\sqrt{2}}} \tag{35}$$

and the phase angle α_d between H_d and H_0 from equation 34

$$\alpha_d = \frac{d}{p\sqrt{2}} \text{ in radians} \tag{36}$$

Appendix IV

The current density K_0 in the surface layer

$$K_0 = I_0/\text{cross section of path}$$

Using equation 26 and substituting for Φ from equations 30 and 7,

$$K_0 = \frac{(f/\rho)(\Delta r/a)\mu H_0 2a\pi p\, 10^{-8}}{\Delta r\cdot 1cm}$$

$$= \frac{10}{4\pi}H_0 4\pi^2\frac{2\mu f}{\rho}p\,10^{-9}$$

Substituting from equation 31 for p,

$$K_0 = H_0(10/4\pi)/p = (4\pi/10)In(10/4\pi)/p$$
$$= In/p \tag{37}$$

Appendix V

Assigning the same radius a to the various sleeves of infinitesimal thickness (which is permissible without undue error for $p<a/20$), the total heat W:

$$W = 2a\pi\int_{d=0}^{a} w_d d(d) = (In/p)^2\rho\pi a\times \int_{d=0}^{a}\epsilon^{-\frac{d\sqrt{2}}{p}}d(d)$$

$$W = (In/p)^2\rho\pi(-ap/\sqrt{2})\left(\epsilon^{-\frac{a}{p}\sqrt{2}} - 1\right)$$

For $p<a/20$ $\epsilon^{-\frac{a}{p}\sqrt{2}} \approx 0$

$$W = (In/p)^2\rho\pi ap/\sqrt{2} = 4\pi^2 a(I_{eff}n)^2\times \sqrt{\mu f\rho\, 10^9}\,10^{-9} \tag{38}$$

Appendix VI

The referred resistance R_a follows from equation 38:

$$I_{eff}^2 R_a = 4\pi^2 a(I_{eff}n)^2\sqrt{\mu f\rho\, 10^9}\,10^{-9}$$

$$R_a = 4\pi^2 an^2\sqrt{\mu f\rho\, 10^9}\,10^{-9} \text{ ohms} \tag{39}$$

For a copper inductor, using a rectangular water-cooled conductor, $\mu = 1$; the resistivity of the inductor at a temperature of 60 degrees centigrade $\rho = 2.04\ 10^{-6}$ ohm-centimeter, using these values in equation 39:

$$R_b = 1.8\, b\, n^2\sqrt{f}\ (c_1/c_2)\,10^{-6} \tag{40}$$

The flux Φ_0 in the interspace is:

$$\Phi_0 = 0.4\pi In(b^2 - a^2)\pi$$

$$e = -\frac{d\varphi_0}{dt}n10^{-8} = -0.4\pi n^2(b^2 - a^2)\pi\frac{di}{dt}10^{-8}$$

$$E = 8\pi^3 n^2 f(b^2 - a^2)I\,10^{-9} \text{ volts}$$
$$X_0 = E/I = 8\pi^3 n^2 f(b^2 - a^2)10^{-9} \text{ ohms} \tag{41}$$

Appendix VII. Definitions of Symbols

a—radius of the charge, centimeters
B—induction, peak value of sinusoid, gausses
b—radius of the inside of the inductor, centimeters
c_1—winding pitch of the inductor, centimeters
c_2—height of the conductor, centimeters
d—distance of a point inside of charge to surface, centimeters
E—electromotive force, peak value of sinusoid, volts
e—electromotive force, instantaneous value, volts
f—frequency of inductor current, cycles per second
H—magnetic intensity, peak value of sinusoid, oersteds
h—magnetic intensity, instantaneous value, oersteds
I—current, peak value of sinusoid, amperes
I_{eff}—current, rms value, amperes
i—current, instantaneous value, amperes
j—$\sqrt{-1}$
K—current density, peak value of sinusoid, amperes per square centimeter
n—number of turns of inductor per centimeter axial length
P—power, average value, watts
p—penetration, centimeters
Δr—thickness of sleeve, centimeters
R_a—referred resistance of charge, ohms per centimeter axial length
R_b—high-frequency resistance of inductor, ohms per centimeter axial length of inductor
t—time, seconds
w—generated heat, average value, watts per cubic centimeter
X_0—reactance of inductor due to the magnetic field in the interspace between inductor and charge, ohms per centimeter axial length of inductor
Z—total impedance of loaded inductor, ohms per centimeter axial length of inductor and charge
α, β, γ, δ—angles, in degrees or radians, as noted
ϵ—base of natural logarithms, 2.71828
μ—permeability, unity
π—3.14159
Φ—magnetic flux, peak value of sinusoid, maxwells
φ—magnetic flux, instantaneous value, maxwells
ρ—resistivity, ohm-centimeters
ω—angular velocity, radians per second

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Induction Heating—Selection of Frequency

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THE general definition of induction heating is the use of eddy currents to raise the temperature of a conducting mass. Thus induction heating is an application of the theory of the eddy-current phenomenon.

This paper deals with the relation of frequency and kilovolt-ampere capacity as the basis for the selection of the frequency for a given heating service.

Charges. The body (or bodies collectively) to be heated is termed the charge. Charges in induction-heating practice are of three types, namely:

1. Charges of nonmagnetic material.
2. Charges of magnetic material. Maximum temperature below the critical temperature.
3. Charges of magnetic material. Maximum temperature above the critical temperature.

The Solid Cylindrical Charge. The shape of the charge is taken into account in analyses of the heating effect of eddy currents. However, the shape of the charge is not a factor in the principle involved. Hence a simple shape, the solid cylinder, is selected as the basis of this discussion.

The Transformer. The most effective arrangement of parts for induction heating is the charge inside a coil. An assembly of this type, less supports, for heating a charge to a temperature below the melting point of the material is shown in Figure 1.

This assembly is a form of transformer with these peculiar features:

(*a*). A closed secondary circuit formed in the charge.

(*b*). An open magnetic circuit. In some cases iron is added to the portion of the path of the magnetic flux that is outside of the charge. However, the general case is expressed by the term "coreless induction heating."

(*c*). The air gap between the primary and secondary circuits.

Circuit Equations. The equations of the circuits of the transformers previously noted are given in the appendix.

Basic Equations. The relation of the factors in the eddy-current phenomenon is given in the three equations which follow.

Reference dimension,

$$p=\frac{\sqrt{\rho 10^9}}{2\pi\sqrt{2}\sqrt{\mu f}}\text{ centimeters} \qquad (1)$$

$$=\frac{3{,}560\sqrt{\rho}}{\sqrt{\mu f}}\text{ centimeters} \qquad (1a)$$

$$\text{Index ratio, } \Delta=\frac{a}{p} \qquad (2)$$

p = the radial dimension of the annular zone of the charge indicated in Figure 2

ρ = the resistivity, ohm-centimeters^3, of the material of the charge

μ = the permeability of the material of the charge

f = frequency

a = the radius of the charge, centimeters

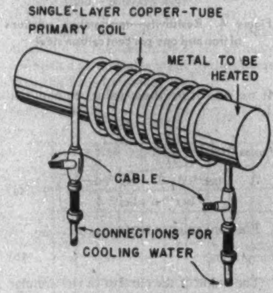
SINGLE-LAYER COPPER-TUBE PRIMARY COIL
METAL TO BE HEATED
CABLE
CONNECTIONS FOR COOLING WATER

Figure 1. **A typical assembly, less supports, for heating solids**

Rate of Heat Development. The rate at which heat is developed by eddy currents in a solid cylindrical charge is expressed by the equation:

$$P_{\text{cm}^3}=\left(\frac{1}{4\pi\sqrt{2}}\right)(H^2)\left(\frac{\sqrt{\mu f}\sqrt{\rho 10^9}}{a}\right)\times [F_r]\times 10^{-7} \qquad (3)$$

P_{cm^3} = watts per cubic centimeter

H = the intensity of the alternating magnetic flux at the peripheral surface of the charge

$$[F_r]=\left[\frac{\text{ber}\,\Delta\,\text{ber}'\,\Delta+\text{bei}\,\Delta\,\text{bei}'\,\Delta}{\overline{\text{ber}}^2\,\Delta+\overline{\text{bei}}^2\,\Delta}\right]$$

Values of $[F_r]$ for a range of values of the index ratio are given in Figure 3.

Resistivity. The resistivities of metals have positive temperature coefficients. This characteristic is illustrated by Figure 4 for iron and carbon steel in the solid state. There is a marked increase of resistivity at the melting points of metals. The resistivities of steels in the molten state are within the range 150 to 200 microhm-centimeters cube. The value in each case depends on the analysis of the steel and its temperature.

The value of ρ to insert in equations 1, 2, and 3 is the value that corresponds to the maximum temperature of the charge.

Permeability. Values of μ are based on uniform distribution of magnetic flux. A feature of induction heating is nonuniformity of flux distribution. This and the fact that with alternating current the permeability changes during every cycle make uncertain the effective value of μ to insert in equations 1, 2, and 3 for type-2 charges. Generally it is assumed that the effective value of μ is some fraction of the saturation value. In any case the value of μ for calculations must be either estimated or determined by measurements.

The effect of the magnetic property in the batch heating of type-3 charges is an initial boosting of the rate of heat development. This effect lasts until the outer layer of the charge reaches the critical temperature. Important as this effect may be, it is not necessary to consider it in circuit calculations. Thus the value μ = 1.0 is used in equations 1 and 3. It is stated in a later paragraph that

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the boosting effect previously noted may be given qualitative consideration with reference to the selection of frequency.

With the continuous-heating method for type-3 charges the effect of permeability on the rate of heat development is continuous but less in degree than in the batch heating of this type of charge. As with type-2 charges, the effective value of μ must be estimated or obtained by measurement.

Operating Index Ratio. The valve of the index ratio which corresponds to the values of specific resistance and permeability noted in the preceding paragraphs, to the given dimension a and to the selected frequency is termed the operating index ratio.

Current Values. Consider a charge of nonmagnetic material, and assume that the temperature of the charge is uniform throughout its mass. For that condition the value of the current in a core of the charge with radial thickness x measured from the vertical axis is given by the equation:

$$i_x=i_a\left(\frac{x}{a}\right)\left[\frac{\overline{\mathrm{ber}}'^2\left(\frac{x}{p}\right)+\overline{\mathrm{bei}}'^2\left(\frac{x}{p}\right)}{\overline{\mathrm{ber}}'^2\,\Delta+\overline{\mathrm{bei}}'^2\,\Delta}\right]^{1/2}\text{ amperes} \tag{4}$$

i_a = total current in the charge

a = radius of charge, centimeters

Volume V_p. The volume of the annular zone of the charge which is defined by the radial dimension p (the reference dimension), see Figure 2, per unit length is

$$V_p=\frac{\pi a^2}{\Delta}\left[2-\frac{1}{\Delta}\right]\text{cubic centimeters} \tag{5}$$

Heat Development in Volume V_p. The value of the radial dimension x in equation 4 with reference to volume V_p is

$$x=(a-p) \tag{6}$$

Hence, in equation 4,

$$\frac{x}{a}=\left(\frac{a-p}{a}\right)=\left(1-\frac{1}{\Delta}\right) \tag{7}$$

and

$$\frac{x}{p}=\left(\frac{a-p}{p}\right)=(\Delta-1) \tag{8}$$

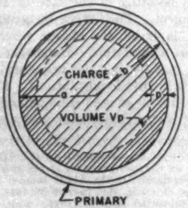

Figure 2. Relationship of the *a*, *b*, and *p* dimensions

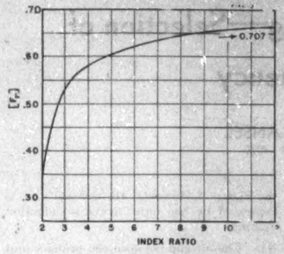

Figure 3. Values of the resistance function [F_r] for solid cylindrical charges

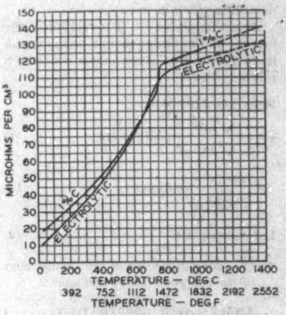

Figure 4. Resistivity—temperature relations of iron and one per cent carbon steel

If we substitute these values in equation 4,

$$i_{(a-p)}=i_a\left(1-\frac{1}{\Delta}\right)\times \left[\frac{\overline{\mathrm{ber}}'^2(\Delta-1)+\overline{\mathrm{bei}}'^2(\Delta-1)}{\overline{\mathrm{ber}}'^2\,\Delta+\overline{\mathrm{bei}}'^2\,\Delta}\right]^{1/2} \tag{4a}$$

For convenience write

$$i_{(a-p)}=gi_a \tag{4b}$$

The value of the current in the annular zone V_p is

$$i_p=(i_a-gi_a)=i_a(1-g)\text{ amperes} \tag{9}$$

The rate of heat development by eddy currents in volume V_p is thus

$$i_p{}^2r_p=i_a{}^2r_a-(gi_a)^2r_a\text{ watts} \tag{10}$$

$$=i_a{}^2r_a(1-g^2)\text{ watts} \tag{10a}$$

r_p = the resistance of the path of the current in annular zone V_p

r_a = the resistance of the path of the current in a core of the charge for any value of x up to and including $x=a$

The quantity $(1-g^2)$ in equation 10a is the fraction of the total heat development that is developed in volume V_p.

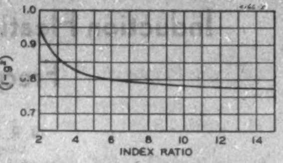

Figure 5. The fraction of the total heat development that is developed in volume V_p

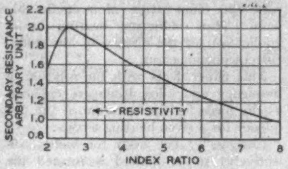

Figure 6. Resistance characteristic of the secondary circuit

Values of this fraction for a range of values of the index ratio are given in Figure 5.

The graph of Figure 5 depicts only the comparative concentrations of heat in the charge for the assumed uniform-temperature condition. The result of the temperature gradient in the mass of the charge during a heat cycle is to increase the concentration of heat development. With this in mind, the concept given by Figure 5 is useful in its indication of the manner in which heat development by eddy currents occurs in a charge.

The Electrical Problem. The factors μ and ρ and the dimension a in the preceding equations are given values for each application of induction heating. The electrical problem is the selection of frequency and the corresponding choice of the relation between volts and amperes for:

(*a*). The required rate of heat development.

(*b*). The desired concentration of heat represented by the graph of Figure 5.

This problem can be contrasted with the problem of minimizing the eddy-current loss in the magnetic structures of electrical machines. Here with given and constant values of frequency, permeability, and resistivity, the factor of selection is the dimension a: for example, one-half the thickness of a sheet for the core of a transformer.

Resistance Characteristic. If resistivity is taken as a variable and the only variable, in equation 26 for the resistance of the secondary circuit of the transformer of Figure 1 the resistance characteristic of that circuit, Figure 6, is obtained.

Electrical Efficiency. The electrical losses of induction heating are the i^2r loss in the primary coil, see Figure 1, and some stray field loss. If iron is included in the outer portion of the magnetic circuit, the consequent core loss is added to the previously noted losses.

When the stray field loss and the core loss, if any, are neglected, the efficiency of the transformation is

$$\eta = \frac{r_a}{r_b + r_a} = \frac{r_a}{R} \qquad (11)$$

r_a = the resistance of the secondary circuit
r_b = the resistance of the primary circuit

The resistance of the primary circuit, equation 23, is proportional to the square root of the frequency. The resistance of the secondary circuit, equation 26 is proportional to the product $[\sqrt{f}[F_r](Y)]$. If we consider frequency as a variable and start with a low value of the index ratio, the value of r_a increases faster than that of r_b and the efficiency of the transformation increase. This continues until with increasing frequency the product $[[F_r](Y)]$ becomes constant, see Figures 3 and 16. Thereafter, the efficiency is unaffected by increase of frequency. This characteristic is shown by curve A of Figure 7.

The one function of frequency in induction heating is to increase the resistance of the secondary circuit to obtain high efficiency of transformation. As the increase of efficiency by increase of frequency ends after a certain value of frequency is reached, there is no object in a higher frequency unless it may give a more desirable relation of volts and amperes or offer some economic advantage.

Power Factor. The power factor measured at the terminals of the primary coil of the transformer of Figure 1 is

$$\cos \varphi = \frac{R}{Z} \qquad (12)$$

R = the total resistance, equation 28
Z = the corresponding impedance, equation 30

The power-factor characteristic corresponding to the efficiency characteristic of Figure 7 is shown by curve B, Figure 7.

Base Frequency. The characteristic of Figure 6 and the characteristics, A and B of Figure 7 indicate that the frequency for a given assembly should be a value that gives an operating index ratio not less than 2.50. Hence, the frequency which corresponds to the index-ratio value 2.50 is designated as the base frequency of an assembly for induction heating.

The equation for the base frequency is obtained by substituting $\Delta = 2.50$ in equation 2, thus:

$$f_B = \frac{79.0 \times 10^6 \times \rho}{a^2} \qquad (13)$$

Example 1. $a = 3$ centimeters, $\rho = 114 \times 10^{-6}$ ohm-cubic centimeters, $\mu = 1.0$.

$$f_B = \frac{79 \times 114}{9} = 1{,}000 \text{ cycles}$$

Base Frequency and Index Ratio. Values of multiples of the base frequency and the corresponding values of the index ratio are given in Figure 8.

Example 2.

$f_B = 1{,}000$
$f_2 = 4{,}000$
$\Delta_2 = \sqrt{4} \times 2.50 = 5.0$

The graphs of Figure 9 show the base frequencies, $\Delta = 2.50$, and the frequencies which correspond to $\Delta = 3.50$ for a range of diameters of carbon steel at around 1,200 degrees centigrade.

Frequency and Current. The watts developed in a charge with the base frequency are

$$P_B = i_B^2 r_{aB} \qquad (14)$$

and with some other frequency, f_2,

$$P_2 = i_2^2 r_{a2} \qquad (15)$$

For the same rate of heat development in each case, that is, $P_2 = P_B$, the value of the current with frequency f_2 is

$$i_2 = i_B \left[\frac{r_{aB}}{r_{a2}}\right]^{1/2} \text{ amperes} \qquad (16)$$

The symbol r_a with appropriate added subscript denotes the resistance of the circuit in the charge, equation 26 of the appendix.

A graph of Equation 16 with reference to the index ratio is shown in Figure 10.

Frequency and Voltage. The voltage measured at the terminals of the primary coil with a given watts input to the charge with the base frequency is

$$e_B = i_B Z_B \text{ volts} \qquad (17)$$

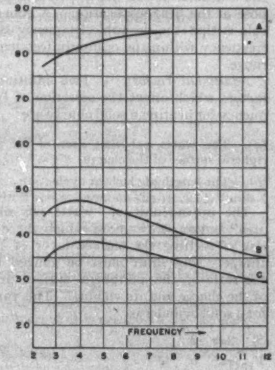

Figure 7

Efficiency–frequency characteristic, curve *A*
Power-factor–frequency characteristic, curve *B*
Curve *C*, the product of curves *A* and *B*

and with some other frequency, f_2,

$$e_2 = i_2 Z_2 \text{ volts} \qquad (18)$$

For the same rate of heat development in the charge in each case, that is, $P_2 = P_B$, as before the voltage with frequency f_2, is

$$e_2 = e_B \left[\frac{r_{aB}}{r_{a2}}\right]^{1/2} \frac{Z_2}{Z_B} \text{ volts} \qquad (19)$$

The symbol Z with appropriate subscript denotes the impedance of the circuit, equation 30 of the appendix.

Frequency and Kilovolt-Amperes. The product of equations 16 and 19 divided by 1,000 gives the relation,

$$\text{kva}_2 = \text{kva}_B \left[\frac{r_{aB}}{r_{a2}}\right] \frac{Z_2}{Z_B} \qquad (20)$$

Example 3. Numerical values for the circuit equations are necessary to

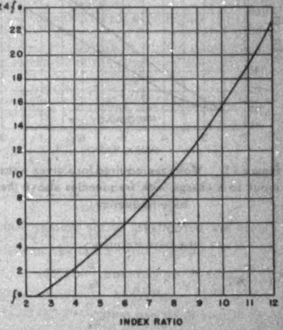

Figure 8. Values of multiples of the base frequency and corresponding values of the index ratio

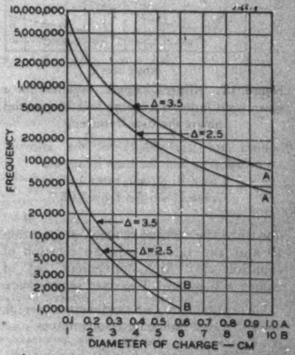

Figure 9. Values of frequencies for the index ratio values 2.50 and 3.50 for a range of diameters of carbon steel at 1,200 degrees centigrade

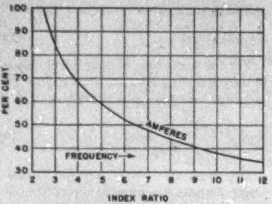

Figure 10. Values of current for a given watts input to a charge with frequencies above the base frequency

Values are percentages of the current with the base frequency

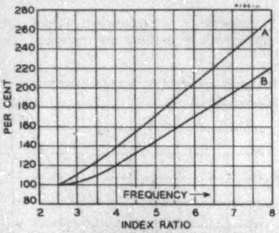

Figure 11. Voltages required for a given watts input to a charge with frequencies above the base frequency

Values are percentages of the voltage with the base frequency

Curve *A*, air-gap ratio, $\frac{b}{a}=1.20$

Curve *B*, air-gap ratio, $\frac{b}{a}=1.05$

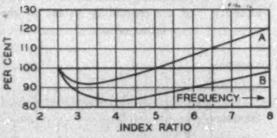

Figure 12. Kilovolt-amperes required for a given watts input to a charge with frequencies above the base frequency

Values are percentages of the kilovolt-amperes with the base frequency
Curve *A*, air-gap ratio 1.20
Curve *B*, air-gap ratio 1.05

illustrate the voltage relation of equation 19 and the kilovolt-ampere relation of equation 20. Any convenient set of values will serve. The data selected are

$a=3$ centimeters, $l=8$ centimeters, $\mu=1.0$,
$\rho=102\times10^{-6}$ ohm-cubic centimeters
$N=6$ $s=0.81$
Air-gap ratios, 1.20 and 1.05

Voltage Relation. The voltages required for a given watts input to a given charge with different frequencies, up to ten times the base frequency, with the two air-gap ratios, are given in Figure 11 as percentages of the voltages required for the given watts input with the base frequency.

Kilovolt-Ampere Relation. The values of kilovolt-amperes required for the given watts input to a charge under the conditions noted for the voltage relation of the preceding paragraph are given in Figure 12.

Example 3, continued,

$\frac{b}{a}=1.20$, curves *A*, Figures 11 and 12,

Base frequency, f_B	900 cycles
New frequency, f_2	8,100 cycles
New index ratio, Δ_2	$\sqrt{9}\times2.60=7.50$
New current, i_2	$0.46i_B$
New voltage, e_2	$2.55e_B$
New kva$_2$	1.17 kva$_B$

Frequency—Efficiency—Power Factor. The kilovolt-ampere relation of Figure 12 can be considered from the viewpoint of the product of efficiency and power factor, equations 11 and 12, respectively. The characteristic thus obtained is shown by curve *C* of Figure 7. This characteristic corresponds to curve *B* of Figure 12.

Optimum Frequency Range. The graphs of Figure 12 do not indicate a well-defined optimum frequency. There is an optimum range of frequency, and the smaller the air-gap ratio the wider is this range.

Service. The two divisions of induction-heating service are:

(*a*). Heating solids to temperatures below the melting points.

(*b*). Melting metals.

Heating Solids. The conditions of this service vary widely, both as regards the character of the charge and the purpose of the heat application. A general rule is that the desirable frequency is a frequency within the optimum frequency range.

Additional Factors. Two additional factors which affect the selection of frequency for heating a solid charge are:

(*a*). The watts per unit area of the peripheral surface of the charge.

(*b*). The length of the heat cycle.

The upper limit of "watts per unit area" is the value above which there is risk of melting the surface layer of the charge. This limit must be gauged by the ratio of the peripheral surface area of the charge and its volume. This ratio for a solid cylinder is

$$\frac{S_c}{V_c}=\frac{2\pi a}{\pi a^2}=\frac{2}{a} \tag{21}$$

The limitations on the length of the heat cycle are:

(*a*). Grain growth.

(*b*). Surface oxidation.

The second limitation is a consequence of the "open-air" method of induction heating applied to solids.

A third factor not hereinbefore-mentioned is the depth of the hardened zone in the special application of heating steel for hardening.

The desirable depth of hardened zone in heating steel for hardening depends mainly on the type of steel and the service for which the part is intended.

These additional factors are matters of experience based on metallurgical considerations and cannot be embodied in general rules for practice. There is much useful information relating to these phases of induction heating in a paper by Vaughn, Farlow, and Meyer.[6]

It is often necessary to consider a range of diameters (thicknesses) of charges of a given material in the selection of frequency. The latitude of the optimum frequency range (if the air-gap ratio is taken into account) together with the degrees of freedom in the choice of "watts per unit area" and the length of heat cycle makes practicable the use of one frequency for a range of sizes of charges. Also, these conditions generally permit the use of a standard frequency in each case.

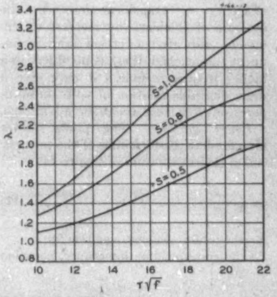

Figure 13. Values of λ for values of the product $\tau\sqrt{f}$

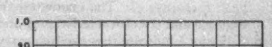

Figure 14. Values of α in equation 23

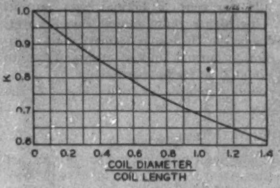

Figure 15. Values of K in equations 25 and 27

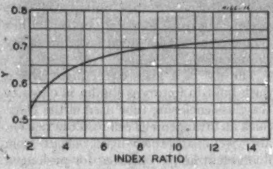

Figure 16. Values of Y in equation 26

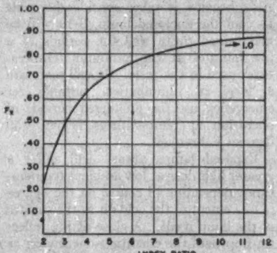

Figure 17. Values of the reactance function $[F_x]$ for solid cylindrical charges

Frequencies below the lower limit of the optimum frequency range are found in practice, particularly for type-3 charges. This practice gives qualitative consideration to the boosting effect of permeability as an aid in the initial rate of heat development. Also, frequencies above the upper limit of the optimum frequency range are in use. There is no technical objection to such frequencies where the practice is considered advantageous.

Melting Service. There are two special requirements which dictate the selection of frequency for metal-melting service. These are:

(*a*). The initial heating of a charge of scrap metal.

(*b*). The stirring of the molten metal by electrodynamic action.

Initially the index ratio is related to the nature of the charge and to the dimensions of the pieces of the charge. The frequency must be a value that gives a transformation efficiency high enough for effective initial heating, that is, to start the heat cycle.

After the charge has reached the liquid stage, the stirring of the metal by electrodynamic action is proportional to the square of the ampere turns. For a given watts input to the charge, the required ampere turns decrease with increase of frequency. The stirring of the liquid metal must be moderate in degree and the frequency must be suited to that requirement.

These two requirements and the economics of the service early led to the adoption in the United States of 960 cycles for steel-melting furnaces above 100-kva rating. The standard frequency for smaller furnaces is 3,000 cycles. Various higher frequencies are used for laboratory furnaces.

Appendix. Circuit Equations

The equations given herein are modifications of the theoretical equations of the circuits of the transformer of Figure 1. These equations were derived from experimental studies of circuits with frequencies up to 9,600 cycles and with charges with lengths not less than the diameters.

Primary Resistance. A single-layer coil with a water-cooled copper-conductor at 50 degrees centigrade is assumed.

The radial thickness of the conductor of this coil for the minimum a-c resistance of the coil is approximately

$$\tau=\frac{15}{\sqrt{f}}\text{ centimeters} \qquad (22)$$

The equation for the a-c resistance of the assumed coil is

$$r_b=[(r_b{}^0-r_s)\alpha+r_s]\text{ ohm} \qquad (23)$$

$$r_b{}^0=\frac{0.0018\sqrt{f}N^2b}{l\sqrt{s}\,10^3}\text{ ohm} \qquad (24)$$

f = frequency
N = number of turns
b = inside radius of coil, centimeters
l = length of coil, centimeters
s = space factor of the winding
$r_s=\frac{r_b{}^0}{\lambda}$
λ from Figure 13
τ = the radial thickness of the conductor of the coil
α from Figure 14

This method of calculating the a-c resistance of a coil is based on the work of Burch and Davis.[1]

If the radial thickness of the conductor is less than the dimension given by equation 22, the value of the a-c resistance of the coil approaches more and more closely the value given by equation 24.

Primary Reactance. The reactance of the primary coil is calculated by the equation,

$$x_b=\frac{8\pi^3fN^2b^2}{l\,10^9}(K_b)\text{ ohm} \qquad (25)$$

K_b = correction for the proportion of the coil, Table X of circular 74, National Bureau of Standards.[5] Some of these values are given in Figure 15.

Effective Radius. As a rule with the frequencies used for induction heating the use of the inner radius of the coil in equations 24 and 25 as the effective radius of the path of current in the primary conductor is of sufficient accuracy.

Primary Capacitance. Generally, the capacitance of the primary coil can be neglected. It may be desirable to take it into account with very high frequencies.

Secondary Resistance. The a-c resistance of the circuit formed in the charge is calculated by the equation,

$$r_a=\frac{2\pi\rho N^2}{l}\Delta[F_r](Y)\text{ ohm} \qquad (26)$$

ρ = the resistivity, ohm-cubic centimeters, of the material of the charge at its maximum temperature
N = number of turns in the primary coil
l = length of charge, centimeters
Δ = the index ratio corresponding to the previously noted value of resistivity
$[F_r]$ from Figure 3
Y = a correction factor from Figure 16

Secondary Reactance. The equation for the reactance of the secondary circuit is

$$x_a=\frac{8\pi^3fN^2a^2}{l\,10^9}[F_x](K_a{}')(Y)^{1/2}\text{ ohm}$$

$$[F_x]=\left(1-\frac{2\theta}{\Delta}\right),$$

$$\theta=\left[\frac{\text{ber}\,\Delta\,\text{bei}'\,\Delta-\text{bei}\,\Delta\,\text{ber}'\,\Delta}{\overline{\text{ber}}^2\,\Delta+\overline{\text{bei}}^2\,\Delta}\right] \qquad (27)$$

A graph of values of $[F_x]$ for a range of values of the index ratio is given in Figure 17.

$K_a{}'$ = a correction from Figure 15 on the basis of the ratio,

$$\frac{2a\left(1-\frac{0.50}{\Delta}\right)}{l}$$

Combined circuit, primary and secondary:

$$\text{Resistance, } R=(r_b+r_a)\text{ ohm} \qquad (28)$$

$$\text{Reactance, } X=(x_b-x_a)\text{ ohm} \qquad (29)$$

$$\text{Impedance, } Z=(R^2+X^2)^{\frac{1}{2}}\text{ ohm} \qquad (30)$$

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Gaseous-Discharge Lamps for Airplane Lighting Service

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FLUORESCENT lamps always have been considered desirable for general lighting within the airplane in order to reduce power consumption and utilize the long thin tubular-light sources that fit in so well in the interior of an airplane. Four-hundred-cycle operation has been found by Hays[1,2] to be excellent for fluorescent lamps, providing about 20 per cent greater efficiency than the 60-cycle alternating current for which these lamps were created originally and reducing the size and weight of the ballasts required. However, uncertainty as to lamp operation at low temperatures and particularly at the extremes of low temperature that might be encountered at tremendous altitudes has prevented the general use of these lamps in this field.

The fact that fluorescent lamps now are used for instrument lighting in military airplanes has indicated that the danger of starting and operating trouble is small, but, since these instrument lamps are enclosed within heat-conserving housings, it has been thought that the good performance they give may not be sufficient proof that larger fluorescent lamps will give equally good service in cabin lighting and other similar uses.

Similarly, while glow lamps now are used successfully in certain special applications in airplane lighting and for indicator service, very little has been recorded regarding their performance on the 400-cycle alternating current commonly used in airplanes. Consequently, a study was made of the effect of low-temperature and 400-cycle operation on these two types of lamps in order, if possible, to release them for general use in airplane service.

Fluorescent Lamps

The chief problem with the fluorescent lamp is to determine its performance throughout the normal anticipated range of ambient temperatures and also to carry this investigation down to the extremes of low temperature that might be encountered at high altitudes. In this low-temperature investigation the assumption was made that, in certain emergencies which might occur during the war, the interior of the airplane might reach the same temperature as the outside air. Such a condition probably would occur rarely if ever in civilian operation, but the information always would be of value in determining the practical as well as the theoretical range of usefulness for this type of lamp.

In this investigation standard 15-watt *T*-8 bulb lamps were tested in three representative types of installations. One consisted of the bare lamp exposed to natural flow of the surrounding air. The second consisted of the lamp with a fairly close-fitting reflector designed to conserve some of the heat generated by the lamp. The third consisted of a lamp surrounded by a glass tube sealed at each end so as to minimize the heat loss from the lamp when operated at low temperatures. These lamps were operated at both 400 and 60 cycles for comparison.

The lamps were placed in the cold box and brought to full equilibrium at various temperature points. Where small temperature changes caused large changes in lamp performance the time required to reach a steady state ranged up to an hour. Readings were made of voltage, amperage, and relative light output. Starting characteristics were checked throughout the temperature range. All values reported represent the average of several lamp runs, so that the values indicate quite closely what would be obtained in service.

The lamps were operated at normal wattage. At 400 cycles this required operation at a current of 0.400 ampere at which the lamps generate approximately 120 per cent of normal light output. This higher than normal current overloads the electrodes somewhat. Operation at normal amperes corresponding to about 13 watts and normal lumens may be found preferable for civilian use after the war.

LIGHT OUTPUT

The tests revealed the important fact that this 15-watt *T*-8 lamp will stay lighted down to the extremely low temperatures encountered at high altitudes on either 60 or 400-cycle current. At the higher frequency they show a slightly better maintenance of light output at low temperatures and a larger factor of safety in operation.

The use of a glass cover plate over a flush built-in fixture is similar in effect to the glass enclosing tube used in the tests. Such an arrangement will result in reasonably well-maintained light output from fluorescent lamps operated for prolonged periods of time at ambient temperatures well below zero degrees Fahrenheit. Also, because it takes considerable time to cool the enclosed lamp, light will be maintained quite well for periods of a few minutes at the minimum temperatures used in the tests. It will maintain a level of illumination suitable for emergency use for prolonged periods of operation at temperatures below −50 degrees Fahrenheit.

Exposed lamps always will suffer a greater light loss at low ambient temperatures, depending of course on the degree of movement of the cold air at the surface of the lamp itself. A close-fitting reflector improves the conditions somewhat as shown by the curves. In general, such an arrangement of the reflector to conserve heat is well worth while and is recommended lighting practice to reduce glare.

LAMP OPERATION

Low-Temperature Starting. Fluorescent lamps are designed to be started by means of a starting switch of some sort. This may be a simple manual switch or an automatic device such as the standard glow switch or the thermal switch, which

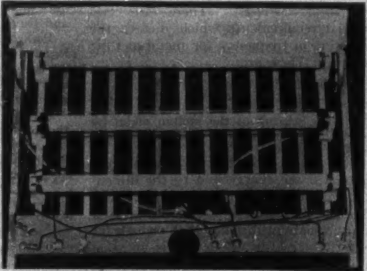

Figure 1. Test lamps were mounted on open wooden rack to test effect of cold surrounding air on lamp performance

Front lamp bare; middle lamp protected by glass-tube jacket; rear lamp operated beneath close-fitting reflector, reflector turned back to show mounting

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The author acknowledges experimental work done by V. H. Laughner, K. F. Molton, and A. J. Dusault.

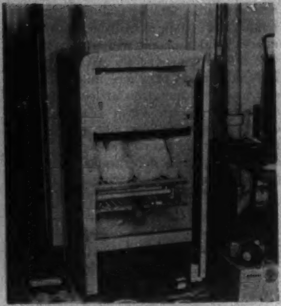


Figure 2. Lamp rack installed in cold box

Door opened to show test setup. Equilibrium reached for each reading, requiring in some cases a period of two hours

Figure 3. All lamp characteristics measured by meters outside cold box

Photoelectric cell and thermocouple inside chamber measured relative light output and ambient temperature at the lamp

is frequently used under adverse starting conditions such as at low temperatures. Various standard methods and modified schemes of starting were tested to determine starting reliability over the test range of temperature.

Lamp starting is critically affected by line surges, by capacitative effect of metal such as reflectors located adjacent to the lamp bulb, and by static effects in the air particularly as influenced by humidity. Not all possible combinations of circumstances were explored, and so this report on starting is merely a record of experiments run. It is probable, however, that in ordinary operating conditions the results will be about the same as found in these tests.

Starting of fluorescent lamps with the standard glow switch on 118-volt 60-cycle lines generally becomes somewhat undependable below 40 degrees Fahrenheit. Four-hundred-cycle operation is somewhat more favorable for starting, but good practice is to recommend glow-switch starters for temperature ranges down to 50 degrees Fahrenheit. The circuit is generally familiar and is shown in Figure 4. Below this point other starters should be used on both 60 and 400 cycles.

On 60-cycle circuits low-temperature conditions require the use of a thermal starter. This greatly improves starting and eliminates practically all danger of recycling of the starter. With such starters on 60 cycles standard 15-watt lamps and equipment are dependable down to zero degrees Fahrenheit. On 400 cycles, however, the thermal starter alone is not appreciably more effective than the glow switch, so other methods of starting are required for dependable starting at low temperatures.

For low-temperature operating conditions on 400 cycles a small series capacitor provides dependable starting with either the manual or the automatic thermal starter. Such a device is shown in the diagram in Figure 5. For the 15-watt *T*-8 lamp tested, a capacitor of one microfarad starts the lamp readily down to about −10 degrees Fahrenheit. A capacitor of $1^1/_2$ microfarad starts it down below −30 degrees Fahrenheit.

The use of the series capacitor for starting is highly effective for 400-cycle operation, but if the lamps are turned on and off frequently it tends to shorten lamp life and so is recommended only if assurance of dependable lamp starting at low temperatures is more important than long lamp life. Normal life on standard circuits at normal amperes (0.30 *A*) is 2,500 hours with the lamp turned on and off once for each three hours of burning. Using the $1^1/_2$-microfarad capacitor described previously and operating the lamps at 0.40 ampere, it is probable that lamp life will be reduced to an average of approximately 1,000 burning hours. Where a longer lamp life is desired the lamps may be operated at lower wattage.

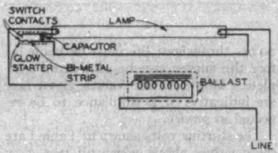


Figure 4. Conventional glow switch and choke circuit for fluorescent lamps suitable for 60 or 400 cycles

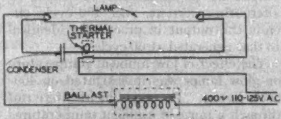
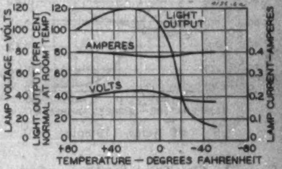


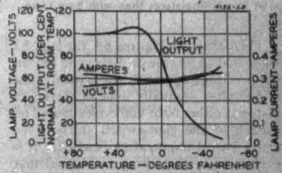
Figure 5. Special 400-cycle circuit to provide dependable starting of fluorescent lamps at low temperatures

Steadiness and Recycling of Starters

No evidence of unsteady operation was noted during these tests. The light meter cell was inside the chamber and showed no minor fluctuations of light output. The voltmeter and ammeter also gave good indication of steady operation throughout the entire temperature range. In addition the door often was opened for a moment to observe conditions, and in no instance did unsteady operation show up.



A. 400 cycles, 118 volts

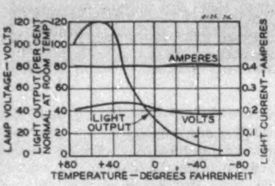


B. 60 cycles, 118 volts

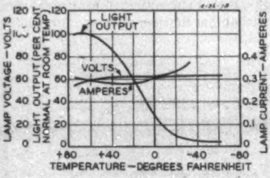
Figure 6. An enclosed fluorescent lamp maintains its light output at low temperatures, particularly on 400-cycle circuits

Tests run with clear glass cylinder around lamp as shown in Figure 1. Still air throughout all tests

Recycling of the standard glow-switch starter at low temperatures results when the cold raises the lamp arc voltage to a value equal to or above the breakdown voltage of the gas (neon or argon) in the glow switch itself. This causes the lamp to flash alternately on and off. The 15-watt *T*-8 lamp has a normal 60-cycle voltage of 56 volts, while at 400 cycles the voltage is only 43 volts. The minimum breakdown voltage of the neon filling gas with the electrodes used in the standard *FS*-2 starter switches is 80 volts at 60 cycles and somewhat higher at 400 cycles. These breakdown volts are practically unchanged at any ambient temperatures that may be encountered. This provides a wide factor of safety with glow-switch starters in 400-cycle operation.



A. 400 cycles, 118 volts



B. 60 cycles, 118 volts

Figure 7. Close-fitting hood-type reflector above lamp tends to conserve lamp heat and maintain level of illumination somewhat better at low temperatures than with bare lamp

The curves in Figures 6A through 8A show that in no case on 400 cycles did the lamp voltage approach the critical value of 80 volts. Actually none of the lamps tested at either 60 or 400 cycles caused recycling of the glow-switch starter even at the lowest temperature reached. It is evident from the voltages obtained that there will be no danger with this size lamp of recycling in service, and it may be that on 400 cycles larger lamps, such as the 20-watt *T*-12 size can be operated without recycling at lower temperatures than on 60-cycle circuits.

Glow Lamps

Glow lamps are particularly well suited to 118-volt a-c airplane indicator service, because they consume very little power and are extremely rugged even in the small sizes generally used for indicator lights. Consequently, investigations were made to determine the effect of 400-cycle power on lamp operation and also the effect of low ambient temperatures on the lamp performance. Table I shows the starting volts above which dependable starting will be obtained for direct current, 60-cycle, and 400-cycle alternating current for several types of glow lamps suggested for airplane indicator service.

These values reported in Table I were obtained from several samples of each of the lamp types shown. Because of the inherent variation of the glow lamp, many more samplings and readings at various points throughout life must be made before the survey can be considered complete. However, the values given here are indicative of performance to be expected in service.

Table I

Lamp Type	Catalog No.	Approximate Starting Volts D-C	60 Cycles	400 Cycles
¼ watt, *T*-4½, candelabra, screw, neon	*NE*-45	90	65	76
½ watt, *G*-10, medium, screw, neon	*NE*-27	—	105	135
1 watt, *G*-10, medium, screw, neon	*NE*-30	85	60	69
2 watts, *S*-14, medium, screw, neon	*NE*-34	85	60	73

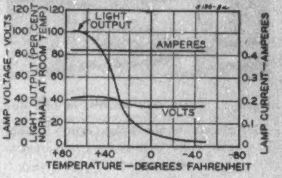
The starting volts shown in Table I are those values above which all new glow lamps should start. Individuals frequently start at lower voltages. On direct current only one electrode (negative) glows, producing approximately the same light output as the combined electrodes on alternating current. On 400-cycle circuits the output is practically identical to that generated at 60 cycles.

The effect of low ambient temperature on glow lamps was investigated on 400-cycle power. The experiments were run through a range of ambient temperatures from 70 degrees Fahrenheit down to −60 degrees Fahrenheit. Throughout this range the change in the light output and the operating characteristics of these glow lamps was negligible, even though the lamps were not enclosed but were exposed to the air.

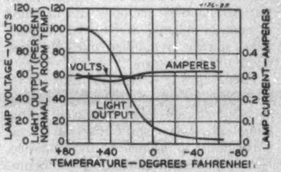
Conclusions

Fluorescent Lamps

It is evident from the experiments run that satisfactory light output, high efficiency of light production, and dependable operation of fluorescent lamps are readily obtainable on 400-cycle airplane circuits. Ordinary equipment with special lightweight efficient 400-cycle chokes is adequate for ordinary ranges of temperature such as will be encountered in areas which people will occupy in comfort. Where provisions are to be made for emergencies or where military necessity involves operation at lower ambient temperatures, the lamp should be enclosed to maintain light output, and manual or thermal starter switches with a small series capacitor should be used to provide dependable starting.



A. 400 cycles, 118 volts



B. 60 cycles, 118 volts

Figure 8. Simple mounting of bare lamp is satisfactory for normal ambient temperatures, although shielding of light from view of passengers is desirable to eliminate glare

Lower lamp volts on 400 cycles shown in curves eliminates all danger of recycling of glow switch at extremely low temperature ranges

GLOW LAMPS

Glow lamps are practically unaffected by low ambient temperatures, and their performance is excellent at 400 cycles. These small rugged low-wattage long-lived light sources are recommended particularly as indicator lights on circuits of relatively high voltage such as on the 118-volt a-c lighting systems of new large-sized airplanes. In that application they will provide a maximum of dependability with a minimum of power consumption.

References

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2. HIGH FREQUENCY ADAPTS FLUORESCENT LAMPS TO AIRCRAFT, R. F. Hays. ELECTRICAL ENGINEERING, volume 60, November 1941, page 539.

Hot-Spot Temperatures in Dry-Type Transformer Windings

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Synopsis: With the increasing use of dry-type transformers, attention has been focused upon the fact that the difference between the average temperature rise by resistance and the hottest-spot temperature rise is much greater than in liquid-immersed transformers.

In this paper there are given the results of an extended series of thermal tests on a variety of dry-type transformer coils. Data are presented in regard to the effect of different heights of coil stacks, different configurations, and, in addition, some essential precautions in making temperature measurements are pointed out. Hot-spot temperatures were investigated in coils cooled by natural draft and by forced-air circulation at different velocities for both directed flow and random flow.

Sufficient data are given to determine the ratio between the average temperature rise of the windings and the hottest-spot temperature rise. Based upon this relationship, the need is shown for review and modification of the conventional hot-spot allowances for dry-type transformers.

In the last few years there have been major advances in the manufacture and availability of class *B* insulating materials, together with improvements in the design of structures using these materials.

This has made possible a large increase in the upper limit of voltage and output ratings of dry-type transformers. A wider range of applications is now possible and correspondingly, an increasingly great volume of these units is being built. These transformers are no longer special cases but should be regarded as an important class of transformers meriting serious and careful attention to their special problems and limitations, as well as attention to their possible advantages.

Previous to these developments, dry-type transformers were cooled by the conduction of heat through solid insulation to the enclosing case or were for the most part sufficiently small so that adequate cooling was easily obtained. With the advent of higher voltages and larger output ratings, satisfactory cooling becomes a major problem and unless such transformers are conservatively designed, winding hot-spot temperatures will exceed established standards. Excessive hot-spot temperatures will, of course, hasten the deterioration of the insulation and shorten the life expectancy of the transformer.

Present Hot-Spot-Temperature Standards

At the present time, American Standards for Transformers[1] specifies a 55-degree-centigrade average temperature rise by resistance and a 10-degree-centigrade hot-spot allowance for transformers with class *A* insulation. These same standards give a limit of 80 degrees centigrade rise by resistance of transformer windings with class *B* insulation but no limit is given for either hot-spot increment or for hot-spot temperature rise. Data in this paper will show that the hot-spot increment for average temperature rises of 55 degrees centigrade and 80 degrees centigrade for dry-type air-cooled transformers using class *A* and class *B* insulations respectively will greatly exceed 10 degrees centigrade for the commonly used arrangements of windings.

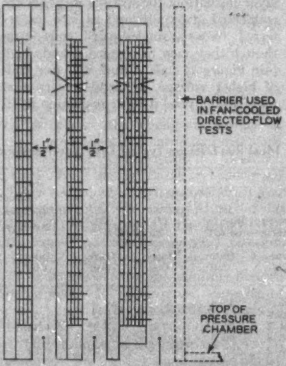

Figure 1. Typical arrangement of coils for tests, showing barrel coils with thermocouple locations

Temperature Measurement of Bodies in Air

It is necessary to take many precautions in the measurement of hot spots in windings cooled by air. First, it is highly desirable to insert measuring means into the winding itself. Measurement of the surface of the winding leads to inaccuracies since the radial gradient of these windings may be considerable, particularly when forced-draft cooling is used. Also, it is not clear whether it is the coil surface temperature or whether it is the temperature of the air film at the surface that is being measured. Coverings on the thermometer or thermocouple bulb reduce but do not eliminate this uncertainty. All measurements of winding hot spots as described in this paper were made by thermocouples buried in the winding and secured in place at the time of winding. Only turn insulation plus a very thin layer of asbestos separated the thermocouple from the copper of the winding in all cases. This totaled from 12 to 15 mils. When a thermocouple is in the maximum temperature level where there is no transfer of heat, the adjacent copper temperature is, of course, reached.

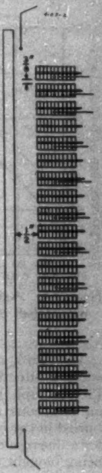

Figure 2. Disk coil showing thermocouple locations

All windings used in these tests were wound noninductively. This reduced the voltages required to approximately the *IR* drop of the conductors, permitted the use of alternating currents, and allowed thermocouple measurements to be made continuously while power was on without the necessity of taking cooling curves to correct back to shutdown for determining hot spots.

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The authors acknowledge the technical advice relative to the measurement of temperatures received from V. M. Montsinger and A. U. Welch of the power transformer division of the General Electric Company, Pittsfield, Mass.

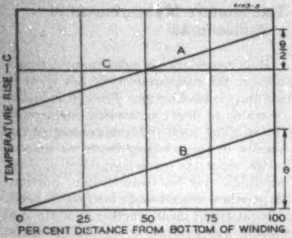

Figure 3. Previously proposed but incorrect axial winding and air gradients

A—Winding temperature rise by thermocouple
B—Air-duct temperature rise by thermocouple
C—Average winding temperature rise by resistance

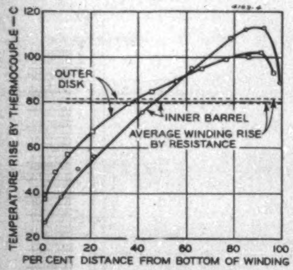

Figure 4. Axial winding gradients with natural-draft cooling for 30½-inch coils

To guard against the thermocouple wires conducting heat to or from the thermocouple bead and hence indicating an erroneous reading, these wires were buried in the coil in an isothermal region for a distance of at least six inches before being brought to the coil surface.

Average temperatures of the windings were determined by resistance measurements with cooling curves taken to correct temperature back to shutdown.

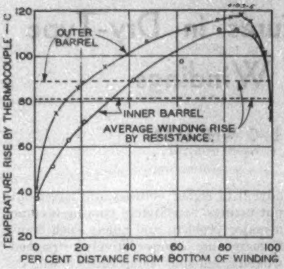

Figure 5. Axial winding gradients with fan cooling for 30½-inch barrel coils

In the case of air-temperature measurements, thermometer or thermocouple in an air stream will measure the temperature of the air only if it is protected from receiving or giving up heat due to radiation to or from other bodies. A silvered tube one inch long and three-eighths inch in diameter was used around the thermocouple bead in these tests with the bead held in a central position by means of cross threads. This allowed the air stream to contact the thermocouple but shielded it from receiving or losing much heat by radiation. In the configurations tested, an unshielded thermocouple indicated air temperatures several degrees in error.

Types of Windings

Two general types of coils are used in air-cooled dry-type transformers. The first type is coils having vertical ducts only which are hereafter referred to as "barrel" coils. They may be wound with or without layer insulation. Without layer insulation, the strands are generally connected in parallel and as such are referred to as "single-layer" coils even though they may be several strands high (see Figure 1, coils *A* and *B*). For coils of higher voltage, a barrel construction may still be used if layer insulation is introduced between layers. Such a coil is hereafter referred to as a "multilayer" coil (see Figure 1, coil *C*).

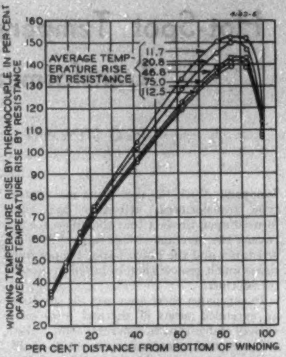

Figure 6. Axial winding gradients at various loads with natural-draft cooling for 30½-inch barrel coils

A second type of coil has an arrangement of horizontal ducts, as well as a vertical duct. These coils are hereafter referred to as "disk" coils (see Figure 2). Such coils are generally used as outside coils only as they usually have poor cooling efficiency if used as inside coils in dry-type transformers.

Types of Coils Tested

Barrel coils were tested in heights of 12½, 30½, and 60½ inches, actual winding lengths, and disk coils in 30½-inch stack height only. These coil heights are typical of dry-type transformers in ratings of 25 to 3,000 kva inclusive.

Ducts were in general one-half inch wide for vertical ducts and three-eighths inch for horizontal.

Arrangement of Coils

Coils were arranged as in Figure 1 for these tests. This arrangement has an in-

Table I. Heat Run Data on Typical Dry-Type Transformer Coils

(1) Winding Height, In.	(2) Air Velocity, Ft Per Min	(3) Average Rise by Resistance, C	(4)	(5) Hot-Spot Rise by Thermocouple, C	(6)	(7) Air Rise, C	(8)	(9) Ratio: (Hot-Spot Rise / Average Rise)	(10)	(11) Hot-Spot Increment, C	(12)	(13) Ratio: (Hot-Spot Increment / Air Rise)	(14)
		Inner Coil	Outer Coil	Inner Coil	Outer Coil	Inner Coil	Outer Coil	Inner Coil	Outer Coil	Inner Coil	Outer Coil	Inner Coil	Outer Coil
12½	†	72.9	70.3	95.0	83.4	53.1	26.0	1.30	1.19	22.1	13.1	0.415	0.50
30½	†	76.0	72.7	106.8	94.3	72.7	21.0	1.40	1.30	30.8	21.6	0.423	1.03
30½	†	74.5	74.3*	104.4	90.3*	77.3	57.3*	1.40	1.21*	29.9	16.0*	0.387	0.28*
60½	†	74.5	74.0	110.6	99.9	83.1	20.4	1.48	1.35	36.1	25.9	0.433	1.27
30½	**	82.6	84.4*	125.2	125.2*	59.8	56.7*	1.52	1.49*	42.6	40.8*	0.713	0.72*
30½	530	86.0	86.4	121.7	119.7	46.3	13.4	1.42	1.39	35.7	33.3	0.77	2.48
30½	2,015	81.2	89.3	111.3	118.3	28.2	22.1	1.37	1.33	30.1	29.0	1.07	1.31
60½	490	68.2	67.7	106.2	101.1	55.9	23.5	1.56	1.49	38.0	33.4	0.680	1.42
60½	1,510	80.4	81.9	120.8	117.9	58.1	20.4	1.50	1.44	40.4	36.0	0.695	1.76

† Natural draft. * Disk-type winding similar to Figure 2. ** External 12-inch fans. Velocity in inner ducts 450 feet per minute.

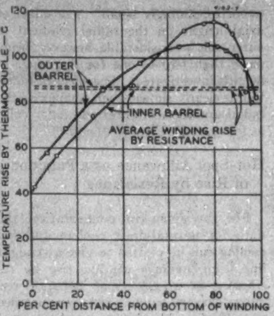

Figure 7. Axial winding gradients with natural-draft cooling for 12½-inch barrel coils

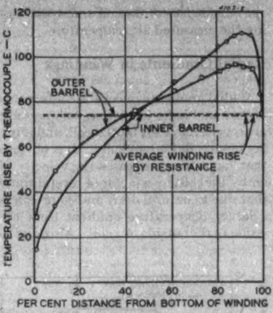

Figure 8. Axial winding gradients with natural-draft cooling for 60½-inch barrel coils

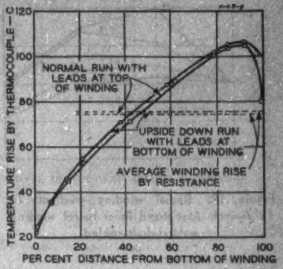

Figure 9. Axial winding gradients with natural-draft cooling showing effect of leads

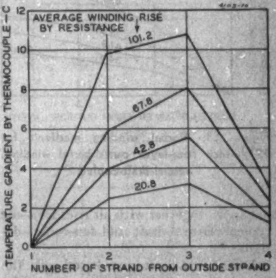

Figure 10. Radial winding gradients for 60½-inch four-strand inner barrel winding, fan-cooled

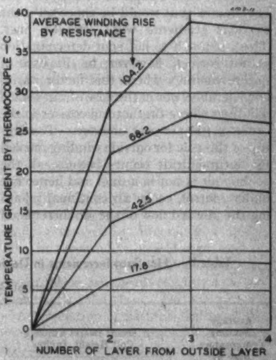

Figure 11. Radial winding gradients for 60½-inch four-layer outer barrel winding, fan-cooled

ner coil *A* which provided the heat that would normally be supplied by a core or an adjacent winding. No core iron was used but a core and yokes of wood were built into each test setup which approximated the air restrictions which would exist in an actual transformer. Windings *A* and *B* were connected in series so that their average temperature rise was within about five per cent of each other. A tap was provided between windings *A* and *B* so that average temperature rise by resistance of each could be determined.

Since the coils were wound noninductively, the currents in windings *A–B* could be adjusted independently of that in winding *C*. In well-designed transformers, the temperature rises of all windings should be approximately equal; therefore, in these tests the currents in all windings were adjusted to give approximately this result.

Types of Cooling

In the smaller sizes, dry-type transformers are usually cooled by natural-draft air circulation. In the larger sizes, fan cooling of some form can be efficiently used. Fan cooling can be generally divided into two types, the so-called directed flow and random flow.

In the directed-flow type of cooling, a motor-driven fan builds up a static pressure in a compartment whose only outlets are through the cooling ducts of the coils and core. Hence, there is a definite and controllable pressure drop across these cooling ducts and consequently a definite and controllable velocity of air flow in all these ducts. If these cooling ducts are designed so as to have equal frictional entrance and exit losses, the air velocities in these ducts will be equal. In practice it may be desirable to design these ducts to have somewhat different air velocities so as to cool some parts of the coils more than others due to differences in loss per unit area of the surfaces. It is thus evident that this method of fan cooling permits accurate design and prediction of winding temperatures. In the test setup an additional cylinder was placed around the outer coil to direct the air flow past this coil and so obtain directed flow.

The random-flow type of cooling utilizes fans blowing air freely against the transformer core and coils. The air streams in this case are directed only by the location of the fans with relation to the windings. Considerable care and a trial-and-error method is necessary to give maximum efficiency to such an arrangement.

Axial Gradients in Windings

It might seem reasonable to assume that the axial gradient in these air-cooled coils would be a straight line whose maximum value at the top of the winding would be the average rise by resistance of the winding plus one half of the temperature rise of the air which flows past these windings. Such incorrectly assumed gradients and relations are shown in Figure 3.

Actual gradients were measured in these tests by a series of thermocouples embedded in the coils. Locations of thermocouples in the barrel and disk coils are shown in Figures 1 and 2. Gradient curves from actual data are shown in Figures 4 and 5 for both natural-draft and fan-cooled units. A marked feature of the vertical coil gradients is the turnover of the curves at the upper end. The hot spot was found to be at some point from 75 to 95 per cent of the winding height, being higher for taller coils. A series of curves taken at various loads is shown in Figure 6.

This turnover at the upper end is due to cooling of the top few turns caused partly by radiation and convection at the end of the coil, but primarily by conduction of heat away by the foundation cylinders and the coil leads. The effect of the leads is strikingly shown by inverting a coil so that leads are alternately at the top and the bottom. The change in the gradients in these two cases is shown in Figure 9.

Table I gives some of the test data on

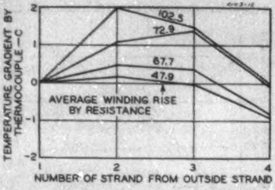

Figure 12. Radial winding gradients for $12^1/_2$-inch four-strand inner barrel winding, natural-draft-cooled

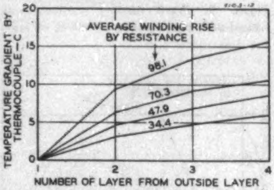

Figure 13. Radial winding gradients for $12^1/_2$-inch four-layer outer barrel winding, natural-draft-cooled

hot spots, together with air rises for some typical natural-draft and fan-cooled designs.

For natural-draft inner-barrel coils, the hot spot can be closely approximated by adding 40 per cent of the temperature rise of the air flowing past these coils to the average temperature rise by resistance of the coil. This compares with the 50 per cent factor that would be expected if the vertical gradients were straight lines. This approach to hot-spot determination is not correct, however, in the case of outer windings where this factor varies considerably, nor in the case of fan-cooled windings where the factor increases to the order of 100 per cent or more. The failure of this rule for outside windings cooled by natural draft occurs because all the cooling air is not in a duct and hence not under control, cooler air continually joining the upward flow of the air currents on the outside face of the coil and so decreasing the measured air temperature.

Radial Gradients in Windings

Radial gradients in coils are a function of turn and layer insulation, loss per unit area, velocity of air by the coil, and number of strands high radially. It is obvious that fan cooling increases the velocity of air by the coils by a factor of several times that due to natural draft and so produces a larger temperature gradient from the inside to the outside of a coil. Also, due to the over-all cooling effect, the loss per unit area of such coils can be raised by a considerable factor without exceeding guaranteed rises. This totals to a considerable radial gradient in the case of fan-cooled units. Typical radial gradients for fan-cooled units are shown in Figures 10 and 11. In all cases the radial gradients shown are taken at or near the height that the maximum axial temperature occurs.

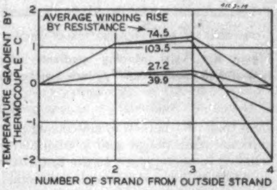

Figure 14. Radial winding gradients for $60^1/_2$-inch four-strand inner barrel winding, natural-draft-cooled

The radial gradients of inner coils are considerably greater in the case of fan cooling as compared with natural-draft cooling. The contrast in the case of outer coils is somewhat less since the radial gradient is already quite large due to loss of heat by the coil through radiation and also its contact with cool ambient air.

The radial gradients of inner coils cooled by natural draft are small and the effect of axial height on the radial gradient is correspondingly small. The effect of axial height on the radial gradient of outer coils is considerable, however, being more than two to one for the range of heights investigated. The effect of radiation is clearly shown when comparing the radial gradients of outer and inner coils, as for example Figures 10 and 11.

Table II. Hot-Spot Increments in Degrees Centigrade for Dry-Type Transformers

Natural-Draft-Cooled

Average Winding Temperature Rise by Resistance, C	Outer Barrel Coils; Coil Height, Inches			Inner Barrel Coils; Coil Height, Inches			Outer Disk Coils; $30^1/_2$ In. Height, 1 In. Build
	$12^1/_2$	$30^1/_2$	$60^1/_2$	$12^1/_2$	$30^1/_2$	$60^1/_2$	
45	9.5	12.5	18.5	16.5	19.5	21.5	10.5
50	10.5	14.0	20.5	18.0	21.5	24.0	11.5
55	11.5	15.5	22.5	19.0	23.0	26.5	12.5
60	12.5	17.0	24.5	20.5	25.0	29.0	13.5
65	13.5	18.5	26.5	21.5	27.0	31.5	14.5
70	14.5	20.0	29.0	22.5	29.0	34.0	15.5
75	15.5	21.5	31.0	24.0	30.5	36.5	16.5
80	16.5	23.0	33.0	25.0	32.5	39.0	17.5
90	19.0	25.5	37.0	27.5	36.0	43.5	19.5
100	21.0	28.5	41.0	30.0	40.0	48.0	21.5

Hot-Spot Allowance as a Function of Rise by Resistance

For any given coil configuration, the data for natural-draft cooling and fan cooling can be plotted to give a straight line using average winding rise by resistance as abscissa and hot-spot rise minus average winding rise by resistance as ordinate. This latter may be termed the "hot-spot increment"; hence, from geometry,

$Y = m X$

where

Y = hot-spot increment
X = average winding rise by resistance

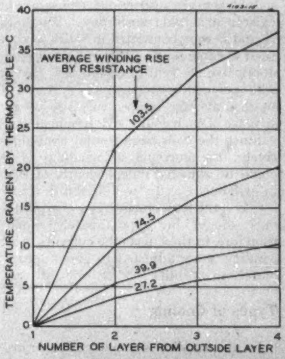

Figure 15. Radial winding gradients for $60^1/_2$-inch four-layer outer barrel winding, natural-draft-cooled

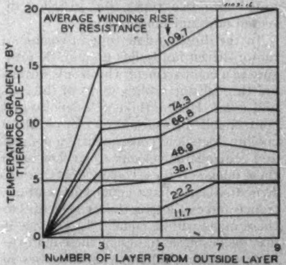

Figure 16. Radial winding gradients for $30^1/_2$-inch ten-layer outer disk-type winding, natural-draft-cooled

Table III. Hot-Spot Increments in Degrees Centigrade for Dry-Type Transformers

Fan-Cooled

Average Winding Temperature Rise by Resistance, C		Outer Barrel Coils; Coil Height, Inches				Inner Barrel Coils; Coil Height, Inches				Outer Disk Coils; 30½ In. Height, 1 In. Build
		$30^1/_2$		$60^1/_2$		$30^1/_2$		$60^1/_2$		
	Air Velocity, Ft Per Min	530	2,015	490	1,515	530	2,015	490	1,515	450
45		16.5	15.5	22.0	21.0	20.0	18.5	25.5	23.0	23.0
50		18.5	17.0	24.5	23.0	22.0	20.0	28.5	25.0	25.5
55		20.5	18.5	27.0	25.5	23.5	22.0	31.5	27.5	28.0
60		22.5	20.0	30.0	27.5	25.5	24.0	34.5	30.0	30.5
65		24.5	21.5	32.5	29.5	27.5	25.5	37.5	32.5	33.0
70		26.5	23.0	35.0	32.0	29.5	27.5	40.5	35.0	35.5
75		28.5	24.5	37.5	34.0	31.5	29.0	43.5	37.5	37.5
80		30.5	26.0	40.0	36.0	33.5	31.0	46.5	40.0	40.5
90		34.5	29.0	45.5	40.5	37.0	34.5	52.5	44.5	45.5
100		38.5	32.0	50.5	45.0	41.0	38.0	58.5	49.5	50.0

$m = 0.21$ to 0.58 depending upon coil configuration (slope of the line)

This same relation may also be expressed as a ratio of hot-spot temperature rise by thermocouple to average rise by resistance as shown in columns 9 and 10 of Table I.

The coil height is a considerable factor in the hot-spot increment. This is true for both inner and outer coils.

These data can be further correlated into bands by noticing that outer coils cooled by natural draft fall into a lower band than inner coils cooled by natural draft (see Figure 20). While these bands are quite wide, it should be noted that test data cover coils of widely differing heights and layer insulation, as well as different types. Disk coils of rather small build and of medium height (approximately one-inch build and $30^1/_2$-inch stack height) were used and this undoubtedly accounts for their being near the lower side of the band (lower curve in Figure 19). It is to be noted that there is about a five-degree-centigrade difference in the hot-spot increment between inner and outer coils in the ordinary range of temperature rises. This is explained by the loss of heat throughout the height of the coil by the radiation component.

Fan-cooled coils were tested in heights of $30^1/_2$ inches and $60^1/_2$ inches only, since at the present time it is believed to be economically feasible to build only the larger kilovolt-ampere sizes of dry-type transformers fan cooled. It is to be expected that there is only a small difference in the hot spots of inner and outer coils cooled by forced draft since in both cases the cooling is mainly by convection, and the cooling by radiation of the outer coil is a small percentage of the total. The data substantiate this idea. Since the radial gradients for fan-cooled coils are higher than for natural draft, the hot spots are slightly more, as might be expected.

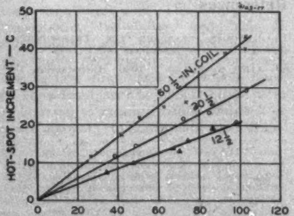

Figure 17. Hot-spot increments for multilayer outer barrel coils, natural-draft-cooled

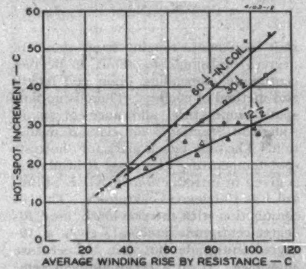

Figure 18. Hot-spot increments for four-strand single-layer inner barrel coils, natural-draft-cooled

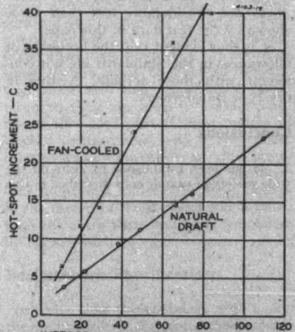

Figure 19. Hot-spot increments for disk coils, natural-draft and fan-cooled

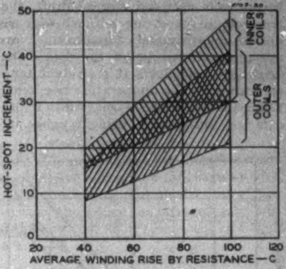

Figure 20. Winding hot-spot increments for natural-draft cooling

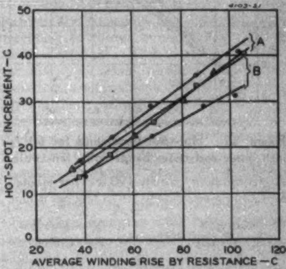

Figure 21. Hot-spot increments for $30^1/_2$-inch inner and outer barrel coils, fan-cooled

A—Cooling-air velocity 530 feet per minute
B—Cooling-air velocity 2,015 feet per minute

A	B
⊙—Inner coil	△—Inner coil
⊡—Outer coil	●—Outer coil

There is some effect of change of air velocity, the lower velocities giving a higher hot-spot. Due to the fact that higher velocities can be somewhat more easily obtained in directed-flow design than in the random-flow design, there is some advantage in the use of this type of construction as a means of control for hot-spot rise.

A correlation of natural-draft and fan-cooled hot-spot increments for $30^1/_2$- and $60^1/_2$-inch coils, as shown in Figure 23, indicates that the fan-cooled designs may have slightly higher hot spots, especially if low-velocity cooling is used.

Air-Duct Rise as a Function of Winding Rise by Resistance

The air-duct temperature rise is a straight-line function of rise by resistance. It is interesting to note that for medium-height coils operated natural draft, the temperature rise in one-half-inch-wide air ducts is approximately equal to the average temperature rise of the coil. The height of coil affects this air-duct temperature rise, the temperature rise being higher for longer coils. Duct width is also a factor, the change being roughly inversely proportional to duct width within a limit of usual dimensions.

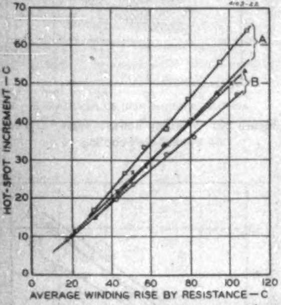

Figure 22. Hot-spot increments for $60^1/_2$-inch inner and outer barrel coils, fan-cooled

A—Cooling-air velocity 490 feet per minute
B—Cooling-air velocity 1,515 feet per minute

⊡—Inner coil ×—Inner coil
△—Outer coil ⊙—Outer coil

Effect of Enclosing Case

The data in this paper were taken mainly with the windings in the open air. This approximates the conditions when expanded metal cases are used. In order to check the effect of a metal-clad case, such as might be provided by a sheet-metal case or the modern-styled louvered case, some runs were made with an enclosing metal shell with openings at top and bottom. The hot-spot increment was not affected within the limits of measurement by the enclosing metal shell.

Discrepancies Between Existing Standards and Test Data

For 55 degrees centigrade rise (class *A* insulation), Tables II and III show hot-spot increments for dry-type transformers ranging from about 11 degrees centigrade for very short natural-draft coils to about 31 degrees centigrade for tall fan-cooled coils. This is contrasted with the flat 10-degree centigrade hot-spot temperature allowance in American Standards for Transformers[1] with class *A* insulation.

For 80 degrees centigrade rise (class *B* insulation), these same tables show hot-spot increments ranging from about 16 degrees centigrade to about 46 degrees centigrade, depending upon coil length and method of cooling. There is no hot-spot temperature allowance given in American Standards for class *B* insulation. The only bench mark for "hottest-spot temperature" for class *B* insulation is given in introduction to AIEE Standard 1 as 130 degrees centigrade. This, in conjunction with the previously used 40-degree-centigrade ambient, gives a 10-degree-centigrade hot-spot temperature allowance. If an average ambient temperature of 30 degrees centigrade is used as given in American Transformer Standards,[1] this allowance becomes 20 degrees centigrade. In most cases, these allowances are considerably less than those shown by the test data in this paper.

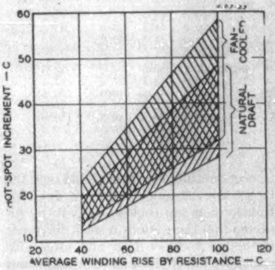

Figure 23. Winding hot-spot increments for natural-draftand fan cooling for $30^1/_2$-inch and $60^1/_2$-inch coils

It is thus evident that the hottest-spot allowances in the Standards are not consistent with those actually existing in dry-type transformers.

Conclusions

1. It has been established by tests on the wide variety of sample coils described in this paper that hot spots much greater than heretofore supposed exist in modern dry-type transformers of normal design dimensions.

2. There is no consistent relationship between the temperature rise of the air in the coil duct and the hot-spot temperature increment. In general, this increment is less than one half the air rise for natural-draft cooling and more than one half the air rise for forced-draft cooling.

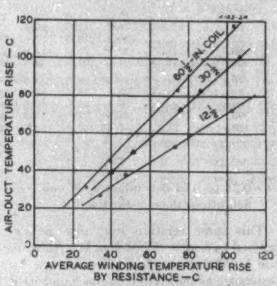

Figure 24. Air-duct temperature rises for barrel-type coils with natural-draft cooling

3. For a given dry-type transformer-coil design, the ratio between the hot-spot temperature rise and the average temperature rise by resistance remains constant when the average temperature rise is varied by changing the load.

4. This ratio increases with coil height.

This ratio is greater for inner coils than for the outside coil.

For a given coil design and air velocities normally used, this ratio is greater for forced-draft than for natural-draft cooling.

This ratio varies somewhat with the type of coil design.

5. No single hot-spot increment applies for all types and ratings of dry-type transformers because of the considerable number of variables affecting the hot spot.

6. The high hot-spot increments found in these tests show the need of review and modification of the conventional allowances for dry-type transformers.

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Some Aspects of the Application of Induction Motors to Aircraft

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Synopsis: Fundamental characteristics of the three-phase induction motor are reviewed with respect to their possible application to aircraft. A brief review of present motor specifications is presented, and test data are given on the operation of an induction motor with one or two lines shot away. Common types of motor loading are discussed with reference to the suitability of induction-motor drive for each. Curves are presented to illustrate the characteristics of motors and various types of loading.

USE of electric power in aircraft has grown with each succeeding year. Aircraft have become larger, and with each succeeding increase in size new and greater demands have been made for electric power. To date these demands have been met magnificently by 12-volt and 24-volt d-c systems, and there seems every reason to suppose that these systems will be in use for a long time to come. However, there is a transmission-weight penalty for a 24-volt system on large airplanes, particularly when the total load is high. Therefore, with an eye to the very large planes of the future, the aircraft industry has been fostering the development of two new electric systems, namely, a 115-volt d-c system, and a 208-volt three-phase 400-cycle system. Either of these two proposed systems will reduce greatly the weight of the electric wiring, and each system has its own special merits.

Motors constitute the principal electric load on military aircraft. A knowledge of motor characteristics is essential to a proper application of these motors to aircraft accessories and to the selection of the electric system itself. This paper presents the fundamental characteristics of the three-phase induction motor and some of the problems involved in applying them to aircraft use. The simplicity of construction, reliability, and lack of commutators and brushes recommends this motor very highly. However, there are certain disadvantages which must be considered, such as, lack of a high ratio of starting to running torque and lack of speed control. In addition to these drawbacks the conditions encountered in applying any piece of apparatus to aircraft must be considered. These conditions are much more severe than those encountered in industrial practice. This point can be understood easily when it is considered that an airplane may take off from a steaming jungle and within an hour be at an altitude of from 20,000 to 40,000 feet, where the temperatures are anywhere from −30 degrees centigrade to −70 degrees centigrade; and in a few hours it may fly far enough, so that when the plane lands the temperature would still be from −50 degrees centigrade to −70 degrees centigrade. Of course, this cycle easily could be reversed. In industrial practice it is thought necessary to rerate the motor with changes in temperature and changes in altitude, but an aircraft motor must operate at its rating under all of these conditions. In addition these motors must be light in weight. Compared with their commercial counterparts, they are mere pygmies. This means that each part of these motors is worked to its utmost with a very small safety factor. Therefore, in applying a motor to a given job, care must be taken that additional material is incorporated in the design in those places which must take the extra wear and tear demanded by the application.

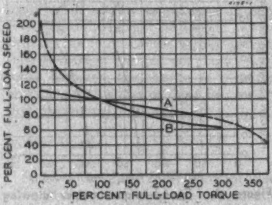

Figure 1. Typical d-c motor speed-torque curves

A. Shunt motor
B. Series motor

Performance Specifications

In order that the engineers who make the applications of the motors, and manufacturers who build the motors, may have a common language and understand each other properly, the United States Army Air Forces together with the manufacturers of motors have written a preliminary performance specification. They first picked a foundation frequency. This frequency is 400 cycles per second, which is a compromise between the 800 cycles per second that has been used for aircraft radio and the 240 cycles per second that was believed best for motors. Since this preliminary specification does tend to give a common language or picture of the motors, and since it does give a basis on which they can be rated, a review of the more common points will be found in Table I.

Speed-Torque Characteristics

For years the aircraft industry has been using small 12- and 24-volt d-c motors to perform the thousand and one odd jobs necessary to the successful flight of the modern airplane. Those who make the various applications of these motors to these jobs are familiar with the characteristics of them and will recognize easily the speed-torque curves given in Figure 1 as the characteristics which apply to the two types of d-c motors most used on aircraft namely, shunt and series motors. In order to apply an a-c motor successfully to these same jobs, the torque characteristics of the a-c motors must become familiar to those who are making the application.

Figure 2 gives typical torque curves for the two classifications of polyphase induction motors given in the table. These curves have been smoothed. Under ordinary conditions the curves for these individual motors are not smooth, but they are filled with small ragged variations caused by the distribution of flux in the motor and the winding distribution of the motor. Figure 3 shows one example of an actual curve of a motor with all the important variations. This curve was taken in the normal manner by taking readings at set points. Therefore, it does not show all of the minute individual

Table I. Motor Performance Specification

	Continuous-Duty Motors	Intermittent-Duty Motors
Frequency	400	400
Line voltage	200	200
Duty cycle	Continuous	1 minute in 20
Allowable frequency variation	±5%	±5%
Allowable voltage variation	±10%	±10%
Combined variation of voltage and frequency	±10%	±10%
Locked rotor torque in per cent of full-load torque	160%	300%
Breakdown torque in per cent of full-load torque	200	None specified
Pull-up torque in per cent of full-load torque	140	None specified

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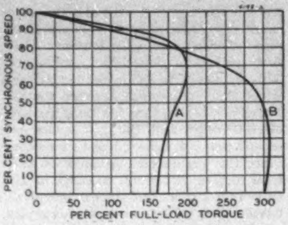

Figure 2. Typical smoothed speed–torque curves for induction motors

A. Continuous-duty motors
B. Intermittent-duty motors

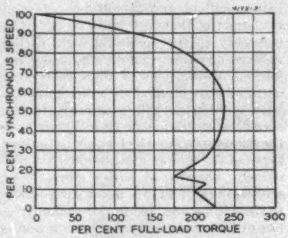

Figure 3. Test speed–torque curve showing irregularities

variations which would be recorded by oscillographic means. In using curves of this type it must be remembered that they apply only at rated voltage unless otherwise specified. When the voltage and frequency variations that are expected are taken into account, these curves vary as shown in Figures 4 and 5. Figures 4 and 5 give the maximum variation of the speed-torque curves for a variation of plus or minus ten per cent in voltage. This is the worst condition which can occur and still be within the limits specified by the Army Air Forces.

It should be emphasized that the curves included to illustrate general performance were obtained by assuming a certain set of constants for these motors. To make any application study a definite design curve should be obtained from the motor supplier.

Motor Operating Speed

The aforementioned curves have been plotted on the basis of per cent rated torque and per cent synchronous speed. For the 400-cycle system, the following motor synchronous speeds are possible: 24,000 rpm, 12,000 rpm, 8,000 rpm, 6,000 rpm, 4,800 rpm, 4,000 rpm, 3,428.50 rpm, and 3,000 rpm. These speeds are the synchronous speeds for 2-, 4-, 6-, 8-, 10-, 12-, 14-, and 16-pole motors, respectively. In general, a lighter power unit can be obtained by using the higher-speed motors and gearing down to the speed desired rather than using a motor with a large number of poles. Some idea of the change in weight with the change in the number of poles can be obtained when it is considered that an eight-pole one-horsepower motor has about $1^3/_4$ times as much active material as a four-pole one-horsepower motor. Nearly all three-phase induction motors run within a few per cent of their synchronous speed at full load. This can be seen easily by examining the curves in Figures 2, 3, 4, and 5. Further examination of these curves show that, even under conditions of high overload, the speed of the motor is still relatively close to the synchronous speed. For this reason an induction motor is known as a constant-speed type of power source. Where speed control is desired this constancy of speed in the induction motor is one of its chief disadvantages. Speed is set by frequency and by certain constants in the motor, and therefore cannot be varied for an individual motor. Compare this constancy of speed with the speed-torque curves as shown for the main types of d-c motors in Figure 1. The series motor has a very large change in speed from no load to full load, and with overload the change becomes correspondingly greater. The shunt motor more closely resembles the induction motor. However, it has relatively large differences of speed between individual motors. This is not true of the induction motor, since its speed is regulated almost entirely by frequency. According to National Electrical Manufacturers Association Standards, the speed of a shunt motor at full load may vary plus or minus 7.5 per cent within a group of motors, whereas for the induction motor the speed variation among a group of motors is apt to be more nearly in the region of one half of one per cent.

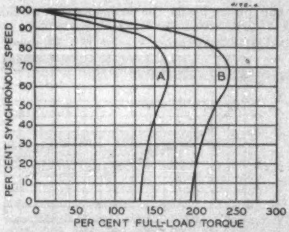

Figure 4. Speed–torque curves showing variation in torque due to voltage variation for continuous-duty motors

A. Speed–torque curve at 90 per cent voltage
B. Speed–torque curve at 110 per cent voltage

In addition all d-c motors are subject to a speed variation with input voltage which may be nearly as great on a percentage basis as the per cent variation of the input voltage. Induction motors' speed varies very little with the input voltage. Figures 4 and 5 show the approximate amount of variation that can be expected. However, the speed of an induction motor does vary with frequency, and, therefore, a variation of approximately plus or minus five per cent can be expected because of frequency change.

Locked-Rotor Inputs

Locked-rotor inputs affect the size of the feeders and protective devices and are, therefore, of interest to the aircraft-system engineer. It is impossible to give aircraft values applying to all cases, but the figures given in the Table II represent normal values that may be expected for three-phase 200-volt 400-cycle induction motors.

If, as often happens, the voltage under locked-rotor conditions is less than rated, the inputs will be reduced; the torque, kilowatts, and kilovolt-amperes will vary as the square of the voltage, the amperes will vary directly as the voltage, and the power factor will remain constant.

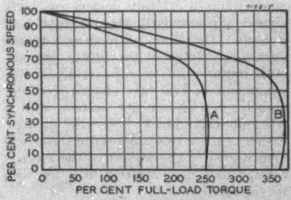

Figure 5. Speed–torque curves showing variation in torque due to voltage for intermittent-duty motors

A. Speed–torque curve at 90 per cent voltage
B. Speed–torque curve at 110 per cent voltage

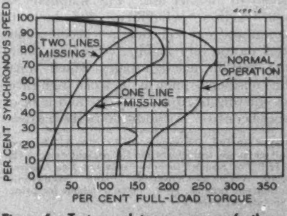

Figure 6. Test speed–torque curves of a three-phase four-wire induction motor showing the effect of having one and two lines shot away

Motor Temperature and Altitude

Most of the motors designed for use in aircraft are designed to be cooled by a self-driven fan. The amount of cooling depends upon the differential of temperature between the cooling air and the motor and upon the density of the air. Motors normally are designed to operate at various altitudes with temperatures per the National Advisory Committee for Aeronautics Standards charts. If the motor is subjected to ambient temperatures other than those given on these charts for the various altitudes, the motor must be more conservatively designed and therefore heavier in weight.

Motor Operation With One and Two Wires Shot Away

In the installation of d-c motors one wire and a ground is usually used. With this installation, if the one wire is shot away, the motor stops. The a-c motor has three line leads and the neutral grounded. If one or two line leads are shot away, the motor will still continue to run, in most cases, though it may not necessarily start if it is once stopped. Figure 6 shows the test speed-torque curves of a motor that was designed to drive a generator which was subjected to overloads of short time duration. This motor was designed, therefore, to have a rather high pullout torque point with respect to full-load torque. Tests were taken with one line open and two lines open. The results are shown in Figure 6. Note particularly the cusp that occurs at one-third synchronous speed with one line open. The size of this cusp varies with the winding of the motor. For some motors the torque actually may go negative at this point. If this occurs in a particular motor it would never accelerate to its normal running speed. A motor having the torque characteristic with one line open such as shown in Figure 6 would accelerate to full running speed providing the torque loading was light. With two lines open, the motor would be running on single phase power and would act as a single phase motor with no auxiliary winding for starting purposes. As long as it is running, it would continue to run provided it was not too heavily loaded. Once it stopped, it would not automatically start itself when power was supplied. A motor operating with either of the two conditions—one or two lines open—would not last long but would soon burn itself out or operate any thermal protection device that might be applied. Considering the multi-circuit wiring systems that have been laid out for the a-c systems, however, the chance of a motor operating with either of these two conditions is extremely remote.

Table II

	Continuous Duty	Intermittent Duty
Torque, in per cent of full load	160	300
Kilowatts per horsepower	2.4	3.5
Power factor	0.50	0.60
Kilovolt-amperes per horsepower	4.8	5.8
Amperes per horsepower	14	17

Motor Loads

Some of the other characteristics that affect the application of the motors can best be brought out by considering some of the different types of loads that occur in an airplane. Many of these loads do not occur singly but in combination. Because of the multiplicity of combinations, only the more common types of load will be illustrated here.

Consider first the fan type of load. This load has a torque characteristic which varies as the square of the speed. Figure 7 shows a continuous-duty motor characteristic with a fan-torque characteristic superimposed. It will be noticed that this motor at sea level is loaded to full load. Quite obviously there is no starting problem for a strictly fan load. But note what happens to the load on the motor at 30,000 feet. Since the induction motor is practically a constant-speed motor, the torque loading goes down as the density decreases. Compare this with the fan operated by a series motor as shown by Figure 8. Here, as the torque loading decreases with density, the motor tends to speed up, thus partially compensating for the decrease in density. Thus with two motors of identical rating the series motor would drive the fan in such a way as to deliver more pounds of air at high altitude. With the induction motor the load at 30,000 feet has dropped to approximately 48 per cent of the sea-level value, while with a series motor it has decreased to only 66 per cent. With a series motor even less drop in load can be obtained by operating on a steeper portion of the torque curve. If such compensation is desired with the induction motor, other means of control must be used.

Another type of load which is encountered very commonly is the so-called friction-type load. This type of loading is usually considered to have constant friction or torque over a large range of speed. If it is remembered that these friction loads generally are made up of bearings and gear losses, it can be seen that they are not constant but vary considerably with the consistency of the lubricant. Very few lubricants are not affected by extremes of temperature encountered in aircraft. Even the friction between two rubbing surfaces changes with temperature and the condition of the surfaces. Thus, the assumption that a friction load is a constant-torque load is false. Only if all conditions of temperature and surface are maintained constant can it be expected that the friction loads will be constant. In making any application of a motor to a friction load the maximum torque required should be deter-

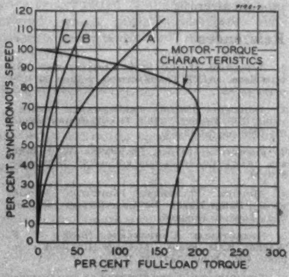

Figure 7. Polyphase induction motor driving a fan

A. Fan speed–torque curve at sea level
B. Fan speed–torque curve at 30,000-feet altitude
C. Fan speed–torque curve at 40,000-feet altitude

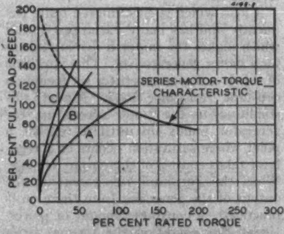

Figure 8. Fan load on a d-c series motor

A. At sea level
B. At 30,000 feet
C. At 40,000 feet

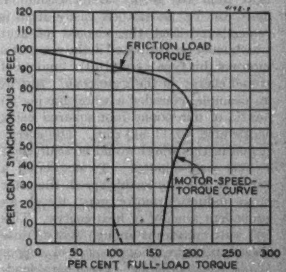

Figure 9. Polyphase induction motor driving a pure friction load

mined for the worst conditions of temperature and surface that can be expected. Most friction loads are characterized by having a relatively high breakaway torque, that is, the coefficient of friction is higher under static than under running conditions. Because of the relatively low starting torque of continuous duty induction motors this may become the limiting factor. Figure 9 illustrates a pure friction load with no allowance for variation due to temperature and surface conditions.

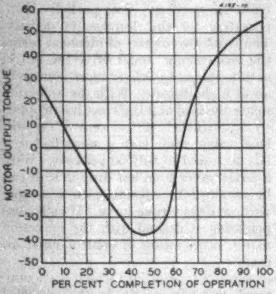

Figure 10. Motor torque in carrying out an intermittent-cycle operation having an overhauling load

Probably one of the most interesting types of application, not so much from a motor standpoint as from a control standpoint, is the operation of inertia-type loads. Gyroscopes driven by motors are used in many of our best weapons today. These units generally require very little running torque, since the only thing which tends to stop them is a slight amount of bearing friction and some windage about the outside surface of the gyro. Therefore, the tendency is to apply relatively very small motors to these units. Care must be taken, however, that adequate surface is provided to dissipate heat from the rotors of these motors. This can be understood easily if it is remembered that the amount of heat which must be dissipated in the rotor of a motor of this type is equal to the amount of stored energy in the gyroscope. Just as an example, consider a wheel having an inertia of one pound-foot squared being accelerated to 11,500 rpm. The energy stored in this wheel would be equivalent to 500 watt-minutes. To accelerate a wheel of this size to this speed, the rotor of an induction motor must absorb an equivalent amount of heat energy. This 500 watt-minutes is enough heat to raise the temperature of one pound of copper 175 degrees centigrade. The general tendency for applications of this sort is to use motors whose rotors weigh much less than one pound. In order to provide the necessary cooling the accelerating time must be extended, or additional cooling area must be provided for the rotor. One of the best tricks for application of this kind is to turn the motor inside out, so that the rotor becomes part of the rotating wheel and therefore has a large mass and heat-storage capacity.

Overhauling loads seldom are encountered by themselves but are generally a part of a cycle operation. Figure 10 illustrates a cycle of operation of an actuator in which an overhauling load occurs. The a-c induction motor is ideal for this type of application, since, if the motor is accelerated to only a few per cent above the synchronous speed, it will absorb approximately as much mechanical power as it would put out if it were running that per cent below synchronous speed. In other words, it becomes an asynchronous generator and feeds electric power back into the rest of the system. Motors handling loads of this type, of course, must be able to deliver sufficient torque to take care of the highest requirements of the cycle. Generally speaking, however, these cycles are of relatively short duration, and the cycles occur so infrequently that an intermittent-type motor can be used satisfactorily. The cycle illustrated in Figure 10 takes about 30 seconds.

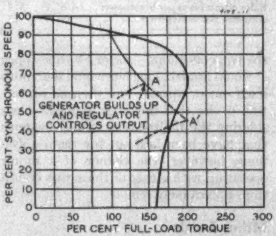

Figure 11. Regulated generator feeding a constant load driven by a three-phase induction motor

A load which is encountered quite frequently and has many variations is that which makes the motor a driving means for a motor generator set. One variation of this is a motor generator set feeding a constant resistance load. Under this condition, as the motor speeds up, the generator builds up until it reaches a point where saturation occurs or a regulator takes control of the output voltage. When this regulator takes control, the output of the generator from there on is practically a constant amount of power. Under this condition the torque varies inversely as the speed. An examination of Figure 11 will show that a condition very easily could be encountered in which the motor generator set would build up to full voltage at a relatively low speed, and the motor would never accelerate beyond point A, as shown on the curve. If the generator built up to full voltage and the regulator took control at approximately 46 per cent of the synchronous speed of the motor, as shown on Figure 11, the point A would coincide with A' at 200 per cent of full-load torque. The motor would accelerate to about 50 per cent of full-load speed and run there. Another variation of the motor-generator-set application is one in which the motor generator set feeds a variable-type load, and the motor generator set is controlled in such a way that under starting conditions the maximum demand can be put on the generator. This type of motor generator set is often used in certain control circuits for actuating devices. Then there is the third type of motor generator set in which the excitation is constant. This motor generator set has a rather definite torque characteristic and does not present the problems which may occur with the others.

The preceding applications illustrate but a few of the many that occur on aircraft. There are a myriad of combinations of the fundamental loads that can occur. Each combination should be broken down and studied with respect to all of the conditions that may be encountered. Induction motors have been used in commercial practice for many years. Their simplicity, ruggedness, and lack of commutating or slip-ring brushes have given them an advantage which far outweighs the few disadvantages they may have. Similarly, in aircraft these same advantages will stand out more clearly with each succeeding application and their reliability and ease of maintenance when compared with the d-c motor with its brushes and commutator will lead to an everspreading use of the induction motor to do the hundreds of odd jobs that are of vital necessity to the modern aircraft.

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Aircraft Storage-Battery Design

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WITH the present great interest in aircraft, not only because of its wartime importance, but also because of the prominence it will have commercially after the war, as well as in postwar military needs, a careful scrutiny of the vital parts that make up the aircraft is in order. One of these parts is obviously the aircraft storage battery. It is used for engine starting, ignition, lighting, and radio.

The paper points out wherein the aircraft battery requirements differ from battery requirements encountered heretofore. Then step by step each of the important parts that goes to make the unit complete is described, and information is given as to what has been done in design to cause these parts to provide a performance of that part to meet aircraft usage.

Attention is called to certain departures that have occurred from what may be considered sound practice, and reasons are given as to how this occurred.

Suggestions are made as to methods of correcting these departures and of experimental work that is being carried on for further improvement.

In summarizing the data and experience accumulated, a suggested design is offered which should overcome the present deficiencies and meet present requirements.

Requirements

The design of aircraft storage batteries involves two major considerations which differentiate the problem from batteries for ground or marine uses. There is obviously first of all weight, and second a nonspill feature which will permit the battery to function at least for a limited time in any position.

Storage batteries usually are associated in one's mind with great weight, but since they are one of the best-known means for the storage of energy, which immediately can be converted to electrical use, intense effort and attention have been directed toward reducing the weight consistent with required service. The resulting watts per pound of weight of the battery compares favorably with other units on the aircraft.

Methods for Weight Reduction

The most apparent and fertile field for work on weight reduction has been the reduction of life expectancy of the battery down to a reasonable point for aircraft use. Like a shoe, the wear of which is proportional to the thickness of the sole, the wear in a battery is proportional to the thickness of the plates. Where life expectancy had been considered in periods of from 5 to 15 years, to consider it in from months to a year allowed much latitude for weight reduction.

Grids

The supporting framework for the active material of a lead storage battery consists of grids of antimonial lead. The long-life heavy battery will have grids of heavy section, and the antimonial content usually will be eight per cent. By increasing this content to 12 per cent, the eutectic point for this alloy, and by allowing specific limited amounts of impurities, such as tin, copper, and arsenic to be present, good castings can be made in very light sections and thicknesses. The grid life will be reduced, but not below a period of reasonable service. Figure 1 illustrates the difference in thickness and member sections of a battery grid designed to give $4^{1}/_{2}$ years of life and an aircraft battery grid.

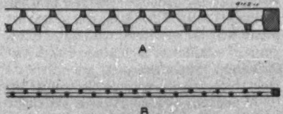

Figure 1

A. A typical positive-grid section designed to give $4^{1}/_{2}$ years of life in reasonable heavy service

B. An aircraft positive-grid section. The lighter section and thickness are obvious

Advantage is taken of the fact that, while the lead peroxide of the positive plate tends to distort its supporting grid by buckling and expanding it, thus requiring a sturdy construction, the sponge lead of the negative does not. The negative grid, since it need only support this sponge lead and act as a conductor, may be constructed of light members with horizontal bars far apart and verticals light in section.

Because aircraft batteries may be charged on a stationary bench in contrast to the vibration they receive in a plane, the practice of including feet on the grids as is done in many other types of service is continued. This permits the two feet of each plate of a given polarity to rest on the same bridges as the rest of the plates of that polarity, while the two feet of the plates of opposite polarity rest on alternate bridges. The sediment that clings to the bridges will in this manner be unable to cause short circuits between plates (see Figure 2).

Active Material

The finished active material of the positive plate is lead peroxide (PbO_2). This

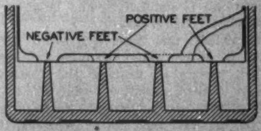

Figure 2. There are four bridges. The feet of the positive plates (two each) rest on alternate bridges. The feet of the negative plates rest on their alternate bridges

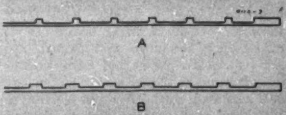

Figure 3

A. The profile of a microporous separator

B. The profile of a wood separator

Since the former has greater mechanical strength, the ribs may be narrower allowing more open space between them and greater acid in contact with the plate

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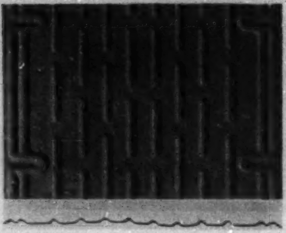

Figure 4. The cross section is constant, decreasing the material used. The ohmic resistance is low because there are no thick sections

is produced by mixing with a solution of sulphuric acid and water an oxide of lead, usually red lead (Pb_3O_4) or litharge (PbO) or both, and further oxidation occurs when this plate serves as an anode in an electrochemical bath using sulphuric acid as an electrolyte. The mixture of oxide and acid solution is made into a heavy paste and applied to the grids with a hand paddle or by a special machine. A wide variation in porosity may be obtained by changing the proportions of red lead and litharge used, the weight of these oxides per unit of volume, and the specific gravity of mixing acid used. Since an aircraft battery is called upon to deliver high currents for short intervals, an open porous peroxide is needed. Therefore, a lightweight red lead usually is selected and mixed with a rather high specific-gravity acid. The resulting large and numerous pores give ready access of the cell electrolyte into the plate. The reaction of the active material with the electrolyte is immediate, and large currents can be delivered.

Obviously this highly expanded peroxide is of less weight than denser ones, which assists in keeping the battery weight down. Active materials like this are of short life compared to dense ones, so the material must not be expanded to a point where it will shed out and fall uselessly into the sediment wells before a reasonable battery life has been obtained.

The finished active material of the negative plate is sponge lead (Pb). Litharge, usually a rather heavy one, is mixed with sulphuric acid pasted into grids to make plates and electrochemically reduced to sponge lead. To "sponge out" the lead, so that the cell electrolyte will have ready access and to further aid in the output of the battery, a small percentage of lamp black and barium sulphate is added. The carbon serves mostly to increase the sponginess but also aids as an electric conductor; the barium sulphate changes the crystalline structure, keeping the crystals small and finely divided. Certain organic materials in small quantities are added to increase the battery output at low temperatures. Among these are wood dust, lignin, hemp, cotton, cork, and humic acid. They are used in quantities of only a fraction of one per cent by weight. Seemingly, they take no part in the reaction, but on discharge will increase the battery output at −20 degrees Fahrenheit to five times that obtainable without them.

Separators

Separators used are microporous rubber or treated wood. Either may be used in conjunction with a glass mat. The mat does not act as a separator but as a retainer of the positive active material. It is applied between the grooved side of the separator and the surface of the positive active material, thus holding mechanically in place material that otherwise would become dislodged and fall into the sediment well. Its use is recommended where long life is desired with but a minimum of sacrifice of high-ampere-rate performance. Since the mats are only 0.03 inch thick and can contain electrolyte up to 90 per cent of their volume, the slight additional internal resistance which they offer is almost offset by the more immediate access they permit of the electrolyte to the positive active material. The use of glass mats may be expected to add 50 per cent to the life of the battery.

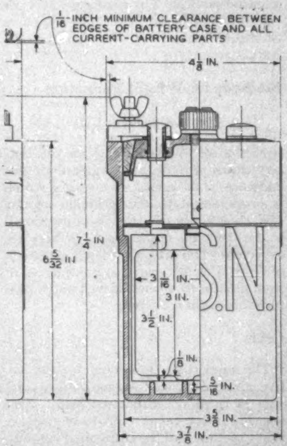
1/16-INCH MINIMUM CLEARANCE BETWEEN EDGES OF BATTERY CASE AND ALL CURRENT-CARRYING PARTS
4 1/8 IN.
7 1/4 IN
6 5/32 IN
3 1/16 IN.
3 IN.
3 1/2 IN.
1/8 IN.
5/16 IN.
3 5/8 IN.
3 7/8 IN.
S.N.

Figure 5. Liberty-motor ignition battery

The space above the electrolyte will hold all of the electrolyte when battery is on its side without submerging vent plug. When upside down, electrolyte will not submerge hole in end of vent tube

Microporous-rubber separators are preferable to wood. They more nearly balance out the life of the separator to the plates than do the wood separators. Their cost is higher than wood, but the increase represents only about five per cent of the cost of the complete battery.

Additional advantages are:

1. The lower ohmic resistance per unit of projected area results in lower internal resistance of the cell and consequently higher cell voltages on discharges.
2. Since they do not require treatment before use, they may be inserted into position between the plates in a dry state.

This permits the battery to be stored dry for an indefinite period without deterioration. The battery can be filled with electrolyte and charged wherever needed with no loss in performance or life, regardless of the storage period.

Batteries insulated with moist wood separators and stored deteriorate seriously after six months of storage.

The wood separator, so far as kind of wood, general profile, and shape, follows that conventionally found in automobile batteries, except that, because of weight and space limitations, it must be made in thinner sections. Preferred wood is cedar, but fir, cypress, and redwood are used. It may be sawed, rotary-cut veneered, or sliced.

To remove natural resins which are harmful to the plates, the wood, after being grooved, is treated in a three per cent solution of caustic. This also serves

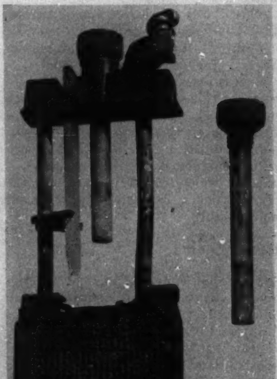

Figure 6. The present German construction is little different from Figure 5. The visual level indicator (rectangular glass plate) is unique

to decrease the ohmic resistance, but after treatment the separator must be kept moist or it will develop a high resistance and become weak mechanically exhibiting splits and cracks.

The usual practice in either microporous rubber or wood is to allow an 0.030-inch back web and a groove 0.040 inch deep. The purpose is to make the separator resistance low and to allow as much electrolyte as possible to come between the plates for ready reaction with the active material. Conventional profile of the separators is shown in Figure 3.

Recent experiments on a microporous separator of a profile as shown in Figure 4 shows promise. Here the cross section through the separator has been kept constant. A 20 per cent decrease in ohmic resistance is achieved. The shape lends itself more readily to the manufacture of synthetic rubber. The volume and weight of material used is lessened by one fifth. As far as tests have progressed results have been good both as to life and discharge. The separator has been produced only on a laboratory basis.

Electrolyte

Sulphuric acid with a specific gravity when the battery is fully charged of 1.285 is used. Permissible impurities in this specific gravity are:

Impurity	Per Cent	Impurity	Per Cent
Iron	0.006	Nitrates	None
Arsenic	0.00005	Ammonia	0.00600
Antimony	0.00050	Chloride	0.01200
Manganese	0.00002	Copper	0.00500

Mechanical Construction

The first requirement for an especially designed lead–acid storage battery for aircraft occurred during World War I with the development of the Liberty motor, where it was desired to supplement the magneto with battery ignition. The battery designed and used was a small eight-volt one capable of delivering three amperes for three hours. Because many of the features found in it are still desirable, it is shown in section in Figure 5.

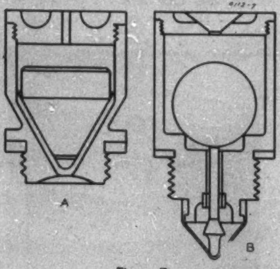

Figure 7

A. The lead weight slides along the inside conical surface of the plug body until it closes the hole in the plug top

B. The lead ball rolls off the valve stem permitting the rubber "spring" at the bottom to close the rubber-seated poppet valve

It will be noted that the nonspill feature was obtained by enlarging the electrolyte expansion chamber sufficiently, so that when the battery was placed on its side the electrolyte that ran from the cell proper was insufficient to rise to the vent plug in the center of the cell. When the battery was inverted the electrolyte was insufficient to rise to the hole in the lower end of the vent tube. Thus at all times the vent remained open for the escape of gas, but the electrolyte could not at any time reach the opening and run out. Since the periods during which the battery was inverted were short, sufficient acid was retained in the plates and separators, so that the reaction could continue and the battery deliver current. The use of glass mats even further aids under these inverted conditions.

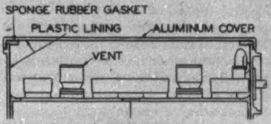

Figure 8. Electrolyte spilled from the vent is confined in the space enclosed by the plastic-lined aluminum container and lid

This space also contains the intercell connectors and terminals, which may become short-circuited if there is much acid

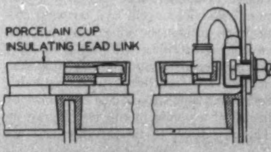

Figure 9. Intercell connector fitted with porcelain cup which is filled with asphaltic compound

The only disadvantages of this type of construction are:

1. The difficulty in obtaining a specific gravity reading, as the normal electrolyte level is well down below the vent plug opening in the cover.

2. The nonspill space required increases the cubical dimensions per ampere-hour as compared with conventional types.

It does not appreciably increase the weight per ampere-hour.

Specific-gravity readings may be taken by placing the battery on its side with vent plug removed, and by using a hydrometer syringe with a curved tube, so that the syringe body is in a vertical position when the tube end is immersed in the electrolyte.

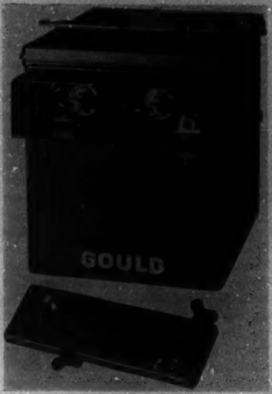

Figure 10. The junction box containing the terminals must of necessity be small, making the application of the cable lugs to the battery terminal studs difficult

It is considered that this construction is still the most effective nonspill one developed.

Figure 6 shows the present German construction. It is interesting to note that they have adhered to this old construction but have added an electrolyte level indicator. The indicator consists simply of a strip of glass extending through the cover down to the normal electrolyte level. In ordinary daylight or with average lighting, the lower end of the glass appears as a greenish-white rectangle upon peering down through its upper end, that is, if the electrolyte is below normal level (the end of the glass strip). When water is added through the vent hole and as the level rises until it touches the lower end of the glass the greenish-white rectangle disappears leaving only darkness. The operator thus knows that the normal level has been reached.

An improvement on the German method has been made by using a rod of methylmethacrylate. By tapering the lower end to a cone and by observing the ensuing dark circle that appears as compared in size with the rod circumference it is possible to judge how far up the cone the level has risen. In this way a level "range" is obtained instead of simply "below normal" or "normal or more."

In passing, comment is made that German lead–acid aircraft batteries compare unfavorably with United States batteries in ampere-hours per pound of weight. This is because of design and not forced substitution of materials. Their principal loss is in their grid construction

For 15 years the desire of plane users has been away from the construction previously described to a battery having only the normal necessary expansion space and the use of vent plugs with check-valve arrangements which, upon rotating the battery, would close before the electrolyte reached them. This does result in a reduction of over-all height, but not a material one, as in incorporating the check-valve arrangement, whatever it may be, additional height must be given to the vent plug.

Figure 7 shows two types that have been used widely. In one case the inverted lead cone slides along the conical surface of the vent-plug body until it comes in contact with the vent top closing the vent hole. This should occur before the electrolyte reaches it. In the other the lead ball rolls off the hard-rubber valve stem allowing the rubber band at the bottom of the plug to force shut the poppet valve mounted on the stem. Even lead is subject to some corrosion by sulphuric acid, making parts stick and become inoperative. Disassembling the plug and cleaning it or discarding it for a new one becomes necessary.

A major objection to the check-valve vent plug is its failure to allow the venting of gas when closed. Some gas is given off from the battery on discharge, and a great deal is given off on charge, the amount being dependent on the amperage of the charging current and the state of charge of the battery. Present generators have extremely high amperage output, and it is not expected that the comparatively small batteries used can absorb it. Voltage regulators are used to control the input to the battery. Should these regulators become inoperative even for a short time, the gas evolved by the battery, even though it is in a partially charged condition, is enormous, and the vent plug has difficulty to relieve it when in an open position. If closed, a pressure is built up quickly which can force the cell cover out of the container, break the seal, and allow complete spillage of the acid.

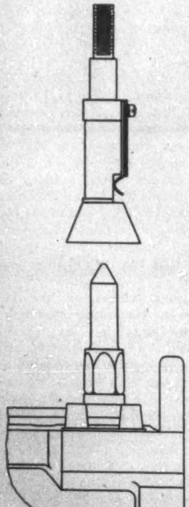

Figure 11. This push-on terminal holds promise

To collect electrolyte spillage from nonfunctioning vent plugs, the aluminum boxes and covers in which the batteries are encased to provide radio shielding are lined with soft rubber or an acidproof plastic. A rubber gasket is applied between the aluminum case and cover, so that, when the hold-downs are drawn up, a seal is obtained which will hold any electrolyte spilled out of the battery proper in the open space above the battery. Figure 8 shows the general construction of aluminum box, cover, acidproof lining, and space where the electrolyte collects. In many instances the accumulation of electrolyte in this space has been so great that drainage tubes have been provided in the aluminum container, so that the spillage can be collected in a separate container away from the battery.

The accumulation of acid on top of the battery may cause the battery to discharge through it from terminal to terminal and cell to cell. All terminal connections inside the aluminum box are covered with rubber or plastic coatings and in some installations the intercell connectors are imbedded in an asphaltic compound held around the connector by means of a porcelain cup (see Figure 9). The objections to this arrangement are the difficulty of removing a connector to open up a cell for examination or repair and the undesirable heat insulation of the connector. Because of the need for light weight, the cross section of the lead connectors is kept small.

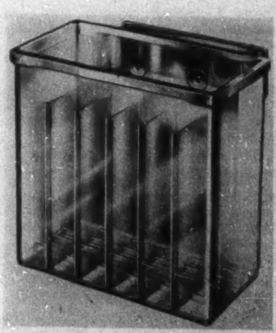

Figure 12. Instead of separate aluminum box, acidproof lining, and internal hard-rubber monobloc, the unit is in one polystyrene piece

Outside Terminal Connections

The present practice is to bring the battery terminals through the aluminum box by means of lead-coated brass studs encased in acid-proof nonconducting bushings drawn down with nuts. The cable ends of the plane harness to the power source and battery load are supplied with terminal lugs and are attached to the protruding studs and held with wing nuts. This outside assembly is encased in an aluminum junction box for radio shielding. The cables enter the box through knockout holes to which the cable shield is attached (see Figure 10).

While this is an effective connection, confined space often makes it awkward as the lid of the junction box must be removed to reach the wing nuts. Other interfering equipment on the plane may make these wing nuts difficult to reach.

Consideration is being given to quick-acting positive connections, such as is shown in Figure 11. It is believed that there will be no weight increase, and that a better and more effective connector will result.

Cell or Battery Container

Hard rubber of good quality having an elongation of six per cent and a tensile strength of 5,000 pounds per square inch has been used for aircraft containers since their inception. It is sufficiently nonacid absorbent to outlast the battery plates. It is reasonably light in weight, does not soften until aforementioned temperatures encountered in satisfactory battery use, and does not become too brittle at low temperatures. Its rigidity

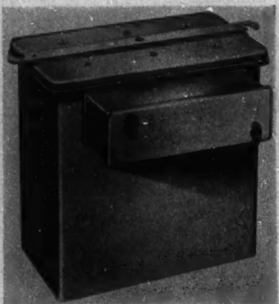

Figure 13. The polystyrene assembly plated with 0.001 inch of lead for radio shielding

is high enough so as not to distort when subjected to hold-down pressures sufficient to hold the battery in place. Could hard rubber of the quality described continue to be available, it is doubtful whether any serious search would be made for a substitute, but the scarcity of natural crude rubber has necessitated dropping the elongation to three per cent and the tensile strength to 4,000 pounds. This has brought about some weight increase.

Much research has been done to find a substitute. Cellulose nitrate has the necessary characteristics for a battery container and long has been used as such. It can be fabricated easily without large tool and mold costs and is plentiful, but it is highly inflammable and hazardous to work with during manufacture and use.

Hydrolized wood has been given attention and is still being investigated.

The most promising material is the plastic polystyrene. It has all of the desirable features of hard rubber plus the fact that it may be used in thinner sections, resulting in a sizable saving in weight. The present problem is not the material itself nor any lack of physical or chemical requirements, but rather the problem of molding.

Polystyrene is a thermoplastic and injection molding of battery cases in sizes smaller than are necessary in aircraft and has been quite successful. Many thousands are being built or are already in use. They are light in weight, transparent (so that the physical condition of the battery may be observed); covers are cemented to the container and become an integral part of it (making sealing leaks impossible), are cleaned easily, thus avoiding external short circuiting.

Aircraft batteries of this material have been built wherein the polystyrene replaced the aluminum box, acid-proof lining, and hard-rubber cell container in one piece. The resultant weight saving was several pounds on a 12-volt 34-ampere-hour battery. All metal in the box construction that might be subject to acid corrosion was eliminated.

The weakness in this polystyrene unit assembly came from the fact that, at the time the work was done, no press large enough to make the box with one injection was available, and from four to six injections had to be made to fill the mold. Strains in the unit resulted, and fractures occurred at low temperatures during vibration. It is believed that, when sufficiently large presses are available to handle a unit of this size with one injection, a conversion to this material and construction may follow. This will be governed also by the supply of available natural rubber or development in the technique of handling synthetic rubbers. Figure 12 shows the unit polystyrene assembly.

The main purpose of the aluminum box in present design is for radio shielding, although as has been pointed out, since it has been provided with an acid-proof lining, its upper portion together with its lid or cover acts as a container for spilled electrolyte. In the case of the polystyrene unit assembly the radio shielding was accomplished by electroplating the outer surface of container and cover with a coating of cadmium. Later lead was used, being preferable as it is acid-resistant and painting of the box was unnecessary. Both of these metals adhered excellently, and rough treatment such as blows with a blunt instrument and scratching did not loosen them nor cause them to peel. The coatings used were from 0.001 to 0.0015 inch thick and added but 2.5 ounces to the container weight; whereas, for the same size battery the aluminum box weighs 3.25 pounds.

Figure 13 shows the polystyrene unit assembly plated with 0.001 inch of lead.

Suggested Design

Many of the objections to present-day aircraft designs are the result of rapidly changing conditions, causing the adoption of one expedient after another. To overcome one weakness some stopgap was used, which in turn had its own drawbacks, and additional adaptations had to be made to correct it.

As a postwar design, or as a war design if materials and tools become available, the following is suggested:

1. Revert to the old nonspill method using the large electrolyte expansion space and the tube vent plug. It will not spill, and gas is vented during inversion.
2. Provide a rod-type visual acid-indicating device.
3. Use a simple positive quick-acting push-on terminal connection.
4. Manufacture the container complete of hard rubber (if available) or polystyrene plastic. If the latter is used, since it is transparent, the visual indicating device may be eliminated, unless radio shielding is necessary.
5. Definitely determine whether radio-battery shielding is necessary. There has been some doubt on this point.
6. If necessary accomplish the shielding by electroplating.
7. Also, if necessary, provide a simple hard-rubber or plastic lid electroplated and clamped to the battery top.

A design such as this will be light in weight, actually nonspill, easy to install and remove, and readily connected to the plane wiring. Figure 14 is a suggested general design.

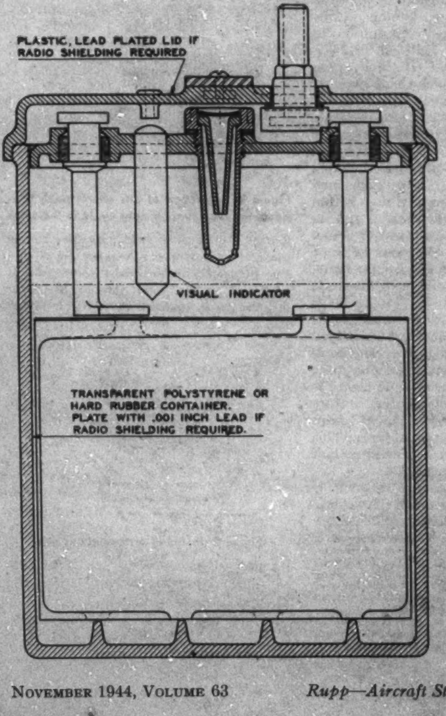
PLASTIC, LEAD PLATED LID IF RADIO SHIELDING REQUIRED
VISUAL INDICATOR
TRANSPARENT POLYSTYRENE OR HARD RUBBER CONTAINER. PLATE WITH .001 INCH LEAD IF RADIO SHIELDING REQUIRED.

Figure 14. This design suggests plastic container, plating for radio shielding, tube-type vent for nonspill, push-on terminals, and visual indicating device

Rating of Electric Equipment Under Intermittent Load

MYRON ZUCKER
MEMBER AIEE

Synopsis: Resistance welding furnishes a clear-cut case of intermittent load. The more elaborate and expensive equipment that marks the trend in welding away from a blacksmith's art to a carefully controlled science warrants increased care in design. This paper explores one aspect of this design: the thermal limits to the periodic load that can be carried. In this case the application is to resistance-welding problems, and illustrative material is drawn from a type of interleaved flexible welding cable that has seen seven years' service in industry. The principles, however, apply to all types of welding: in fact, they extend to many fields in which the problem is to find the reaction to an intermittent force—such as electrical or mechanical transients.

Analysis of Ratings Under Intermittent Load

SHORTCOMINGS OF DUTY CYCLE

WHEN electric equipment is subjected to intermittent load, as in spot-, flash-, projection-, or gun-welding, it is customary to rate it according to the duty cycle[1,2] under which it is to be used. This is good practice as long as we take duty cycle for what it really is: the proportion of *time* that the equipment is heated—or current "on" time divided by total time. But we may get into trouble by extending this definition to say that *temperature rise* is proportional to duty cycle—as is commonly done in rating welding equipment for intermittent load.

In the past, uncertainty of design has called for such large safety factors in welding equipment that the errors inherent in using duty cycle as a load multiplier were unimportant. The advance in the art justifies a review of this situation. Such a review shows that for periodic loads there is a more accurate—and, in many cases, an easier-to-use—method for calculating maximum temperature of the machine.

There are two indictments against duty cycle and its associated averaging time:

1. It does not properly account for the individual heating and cooling curves.
2. It does not properly account for the grouping of impulses.

These two errors work in opposite directions. They frequently compensate each other to a great degree, giving a passable result—which may be the reason why duty cycle has worked as well as it has.

Before undertaking the mathematical analysis of heating under intermittent load, let us get a graphical picture of the errors in duty cycle by studying the temperature reached when load is periodically applied and removed.

Individual Hit Cycles. Figure 1 shows two temperature–time curves for two single hit cycles of different durations but the same *proportion* of "on" time. Contrast the maximum temperatures. Both cases have 33.3 per cent duty cycle, and, according to conventional practice, are considered to impose the same service on the equipment—which they obviously do not. That is the first error.

Groups of Hits. The second weakness concerns series of time patterns such as Figures 2, 3, and 4. For the simplest case (Figure 2, an on–off routine followed for an indefinite time), duty cycle may give nearly correct answers if used within proper limits. But Figures 3 and 4, which are more nearly typical of actual operation because of the rests between limited numbers of hits and groups of hits, is not so well adapted to duty-cycle calculation.

The trouble in this case centers about maximum averaging time over which load is to be averaged when duty cycle is computed. The maximum averaging time assigned to each piece of equipment* is intended to reflect the lower temperature found under intermittent load in equipment having greater heat storage. This intention is correct in a general sort of way.

But it is a chore to use, since it is necessary to draw a picture of the welding operation[3] and slide a maximum averaging time along the time axis to find the part of the picture that includes the most "on" time within the maximum averaging time under consideration.

This limitation on averaging time not only is cumbersome, but gives inconsistent results. It is natural to expect that, as averaging time increases, duty cycle should decrease continuously. But it does no such thing!

Figure 5 illustrates how duty cycle violates the preceding expectation for the pattern of Figure 3 averaged over a wide range of averaging times. The values of duty cycle reach a minimum at multiples of six seconds, stepping up erratically after each multiple, preparatory to a swoop down to the next. An interesting picture, but an absurd standard by which to rate expensive equipment.

Summary. We have, therefore, grounds for questioning the whole duty-cycle procedure. In fact, we will see later that:

1. Duty cycle may be inaccurate by large percentages even in the simplest cases, because:

(a). It does not account for the characteristics of heating.

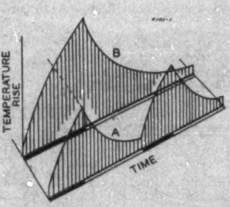

Figure 1. Duration of hits affects peak temperature, even though duty cycle is the same

Curves *A* and *B* show temperature rise on one piece of equipment for two cases where "on" time (indicated by black bars at the base) is one third of total time. Because of longer duration on *B*, its temperature rise is higher than *A*

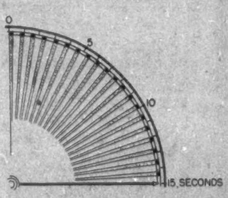

Figure 2 Timing of continuous hits

The timing is described by "on" time 0.25 second, and either:

(a). Off time = 0.5 second
(b). Total time (duration of hit cycle) = 0.75 second
(c). Rate of hits = 80 per minute

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* There seems to have been no definite method of specifying averaging time. One of the few statements in this regard is made by Pike and Ulrey,[3] in discussing the portion of electronic-tube rating that depends on temperature: "The period over which current is averaged must be short enough that the highest temperatures reached will not be in excess of that obtained if steady current equal to the average current rating is kept on continuously." This really requires a different averaging time with each load pattern.

(b). Its basic mathematical assumptions are correct only in certain cases.

2. Duty cycle and its associated averaging time are meaningless, inasmuch as they do not properly account for grouping and series of groups of impulses.

In addition, the duty cycle, even in cases where its poor assumptions are acceptable, can be justified only on the basis of maximum temperatures reached—rather than any weighted average such as has been found necessary in rating other equipment.[4] At the present state of knowledge, this point is only academic in welding problems.

Besides these scientific objections, there are some practical ones: cumbersomeness of application; conflict between this use of the term, "duty cycle," and the AIEE definition as applied to circuit breakers; as well as confusion in terminology between duty cycle and heating cycles consisting of so many (60th-of-a-second) cycles on and off. In an effort to clarify the situation, terms such as "per cent duty"[3] have been used. A change in nomenclature, however, is not enough: the procedure requires complete overhauling.

Duty Factor: Its Use and Meaning

Fortunately, there is a duty factor that is not only easy to use, but flexible, consistent, and correct within engineering limits. This duty factor is the product of a hit factor and a group factor. The partial factors are found from some simple charts, the use and derivation of which will be shown.

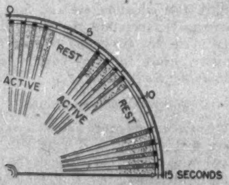


Figure 3. Timing of groups of hits

Hits of Figure 2, with three-second rest after each group of four hits: that is, one group each six seconds. "Active" time includes four complete hit cycles, that is, 4×0.75=3.0 seconds

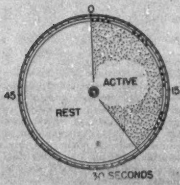


Figure 4. Timing of series of groups

Four groups of Figure 3 repeated every minute. This pattern is duplicated, starting at $t=0$, like a clock. "Active" time=4×6.0=24 seconds

Working Charts

In computing duty factor, the first step is to find the hit duty factor F_h, as though hits were being repeated continuously. This value may be found from either Figure 6 or Figure 7, depending on whether the problem is set up in terms of rest time or of total time or welds per minute. Suppose, for instance, that hits have 0.25 second "on" time and 0.75 second total time (that is, they are made at the rate of 80 per minute). Figure 7A shows that the duty factor for this combination is 0.33: the answer to the case of Figure 2. For Figure 3, a second step is taken.

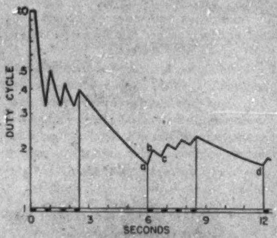


Figure 5. Duty cycle affected erratically by maximum averaging time

For the load pattern of Figure 3, conventional duty cycle sometimes shows big changes (a to b) for small difference in averaging time used in computation; other cases (a to d) show no change for large difference in averaging time. Duty factor avoids this inconsistency

The second step is to find a group duty factor F_g by taking the *active time* as equal to the *number of hits per group* times *duration* of hit cycle. For example, with four of the aforementioned hits in a group, the active time is 4×0.75=3.0 seconds. Using this value, together with total time=6.0 seconds; we find from Figure 7B that the group duty factor is 0.55.

The complete duty factor that measures the temperature to which the equipment will rise under this load, compared to what it would reach under continuous load, is $F=F_hF_g=0.33\times0.55=0.182$.

A similar computation would be made if rest time were used instead of total time. That is, for the same pattern:

1. For hits, we have "on" time=0.25 second, rest time=0.5 second. From Figure 6A, we find $F_h=0.33$.

2. For groups, "active" time=3.0 seconds, rest time=3.0 seconds. From Figure 6B, we find $F_g=0.55$.

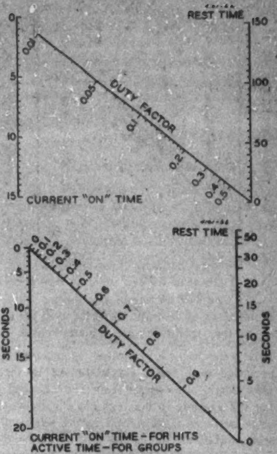


Figure 6. Rest time versus current "on" time versus duty factor, showing fraction of continuous-load temperature rise reached when intermittent load is applied

A. *For short periods.* In welding, this chart usually will be used for single hits, or for a group of hits in pulsation welding

Time scales may be multiplied by any factor provided *both* scales are multiplied. Duty-cycle scale is not changed

For the example given in the text, a straightedge is placed between current "on"=2.5 (that is, 0.25 times a scale multiplier of 10) and rest time=5.0 (that is, 0.5×10). Straightedge then intersects duty-cycle scale at 0.33, which equals *Fh*

This chart may be used directly for equipment having other heating time constants, if periods do not exceed ten per cent of the heating time constant of the equipment involved

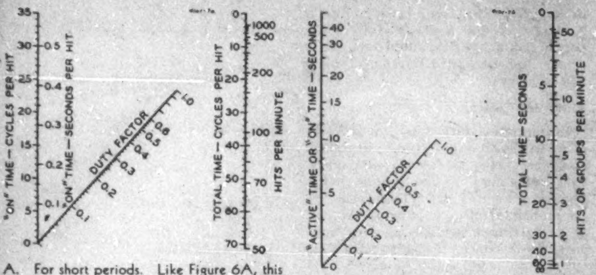
B. *For long periods.* This chart is adapted to groups of hits when there is a rest while changing from weld to weld; or to series of groups when there is a long rest while work is being changed

This chart is designed specifically for interleaved kickless cable, heating time constant=10 seconds. For other equipment, multiply time scales on chart by 10, and use them as (seconds duration (/) time constant in seconds) -scales

The group duty factor for the example in the text is obtained by placing straightedge between three seconds on the "active" time scale and three seconds on the rest time scale. The straightedge crosses duty-factor scale at 0.55

As before, complete duty factor is 0.33×0.55=0.182.

These results are for a light-duty kickless water-cooled welding cable, since the charts are designed for equipment having



A. For short periods. Like Figure 6A, this chart is designed for fast welding operation. "On" time may be measured as either cycles or fractions of seconds, since the two scales on the "total-time" axis are equivalent

Time scales may be multiplied by any factor, provided both scales are multiplied by the same factor. Duty-factor scale is not changed. For the example in the text, place a straightedge between 0.25 second on the left-hand scale, and 80 hits per minute on the right-hand scale. The "hit" duty factor is found to be 0.33, where straightedge intersects the diagonal scale

This chart, like Figure 6A, may be used for any heating time constant, if period does not exceed ten per cent of the time constant of the equipment involved

B. For long periods. This chart serves the same purpose as Figure 6B. It is also designed for kickless cable and is adapted to other equipment by

1. Multiplying time scales by 10 and using them as (duration per heating time constant) scales
2. Dividing the hits per minute scale by 10 and using it as a (hits per minute *times* heating time constant in seconds) scale

The example in the text is solved by a line between "active" time = 3 seconds, and total time = 6 seconds, which indicates a group duty factor of 0.55

Figure 7. Rate of welding versus current "on" time versus duty factor

a time constant of ten seconds which these cables normally have. For other equipment the same charts may be used by changing the time scales as instructed on the charts.

This procedure would be carried one step further if there were series of groups, as in Figure 4. For instance, if after four groups there were an interval of 36 seconds, series multiplier F_s would be obtained in a manner similar to the computation for group factor. In this case, $F_s=0.84$, making the over-all duty factor $F=F_hF_gF_s=0.182\times0.84=0.153$.

These answers may be compared with Figure 5. In this case the duty factor is near the lowest values of "duty cycle"; but this is not a generality—for in many cases the correct duty factor will exceed the duty cycle by a wide margin.

MEANING

The components of duty factor are measures of the temperature that would be attained if the corresponding cycle were to continue indefinitely. The effect of different degrees of continuity is illustrated in Figure 8. If a given load were supplied continuously, temperature rise eventually would be θ_f; if divided into hits, the rise would be only $F_h\theta_f$. (Illustration is for pattern of Figure 2 applied to equipment having such thermal relationships that under continuous load it takes one second for the temperature rise to reach one-half the ultimate value; the rate of rise for this load on ten-second equipment is too slow to permit picturing in space available.) But if, instead of being repeated continuously, hits are broken into groups, the group duty factor tells that only $F_hF_g\theta_f$ would be attained. Further, if there is a rest period between series of groups (Figure 4), we find that the temperature rises only to the fraction F_s of $F_hF_g\theta_f$. With this clue many combinations of periodic on–off patterns can be calculated.

Duty Factor: Derivation

BASIC EQUATIONS

Finding duty factor for any combination of on–off cycles, really means to derive the maximum temperature under those circumstances.

To do this, we start with the basic exponential equation for temperature change in a single transient. The divergence of this equation from actual heating and cooling curves has been aired thoroughly in AIEE meetings[5] and elsewhere,[6,7,8] but still it is the only practical starting point. In fact, so many kinds of equipment do follow nearly exponential rules that perfectly satisfactory calculations can be made in many cases.

The expressions that have been used in studying the heating of power transformers and other apparatus under heavy load[4] are:

For heating, starting from zero temperature rise, $\theta=\theta_f(1-e^{-kt})$ (1)

Table I. Dynamic-Equilibrium Temperature Rises

21,000 Amperes on Light-Duty Cable for One-Sixth Second

Hits Per Minute	Maximum Temperature Rise (Degrees Centigrade) By Test	Calculated by Duty Factor	Calculated by Duty Cycle
3	10.0	10.7	6.3
6	17.1	16.8	12.7

For cooling $\theta=\theta_o e^{-kt}$ (2)

where

θ_f = *final* temperature expected from the constant heat input
θ_o = *initial* temperature at $t=0$
θ = temperature at any time t
$k=0.693T$

T = time required to produce half the ultimate temperature rise when load is applied continuously. It is called the "binary heating time constant."[5] The 0.693 is simply inserted to make equations 1 and 2 have the right values at the right times.

For the present analysis, since heating curves seldom start at zero rise, we will use a more general expression[*] that covers both heating and cooling from any temperature (see Figure 9):

$$\theta=\theta_f(1-e^{-kt})+\theta_o e^{-kt} \qquad (3)$$

With this equation, we can find the maximum temperature rise in a given duty cycle, by *expressing the end temperature of each successive curve in terms of its initial temperature.*

CONTINUOUSLY REPEATED CYCLES

When a heating curve follows a cooling curve and vice versa (Figure 10), the initial value of each curve equals the final value of the other—that is, θ_o for a heating curve is θ_L, and θ_o for cooling is θ_H. Therefore, to find the temperatures at the end of the heating and cooling curves, we use these values for θ_o in equation 3, inserting a for the "on" time and b for the "off" time. This gives

After heating, $\theta_H=\theta_f(1-e^{-ka})+\theta_L e^{-ka}$ (4)

After cooling, $\theta_L=\theta_H e^{-kb}$ (5)

---

[*] These equations assume that heat generated in the equipment is constant over the heating regime. This is a commonly accepted approximation for electric heating, close enough for most cases. When change of resistance with temperature causes material change in heat loss, or where electrical transients cause appreciable change in heat with time, a more refined analysis might be advisable. A partial solution, utilizing a variable time constant for the variable-resistance case, is made by Cooney[9] and Kennelly,[5] for $\theta_o=0$.

Another assumption—common to all applications of the simple exponential equation—is that heat dissipated from the equipment is proportional to temperature rise and that the body is isothermal. Otherwise, the analysis is too complicated for engineering use.[11]

Equation 3 has been developed in many forms. See, for example, reference 10.

To simplify the mathematical development and get usable working charts, we will express temperature rises as fractions of θ_f. Let $\theta_H/\theta_f = H$, and $\theta_L/\theta_f = L$. Then equations 4 and 5 become

$$H = 1 - e^{-ka} + Le^{-ka} = 1 - e^{-ka(1-L)} \tag{6}$$

$$L = He^{-kb} \tag{7}$$

We are now ready to consider the rise and fall of temperature as load is applied and removed periodically. If we keep the identity of each cycle (Figure 11), the initial temperature for the nth repetition of the heating curve is the final temperature for the previous cooling curve, L_{n-1}; the initial value for that $(n-1)$st cooling curve is the high point on the $(n-1)$st heating curve, H_{n-1}. Thus,

$$H_n = 1 - e^{-ka} + L_{n-1}e^{-ka} \tag{8}$$

$$L_{n-1} = H_{n-1}e^{-kb} \tag{9}$$

Then by combining equations 8 and 9, we find H_n, the peak of one cycle, in terms of H_{n-1}, the peak of the preceding cycle:

$$H_n = 1 - e^{-ka} + H_{n-1}e^{-ka}e^{-kb} \tag{10}$$

Equilibrium—if that is the proper word to use when there is a constant variation over a repeated pattern—is reached when successive values of H and L are equal: that is, when we can drop the subscripts. Then, solving equation 10 for H,

$$H = \frac{1 - e^{-ka}}{1 - e^{-ka}e^{-kb}} \tag{11}$$

and

$$L = \frac{1 - e^{-ka}}{e^{kb} - e^{-ka}} \tag{12}$$

H is thus the duty factor F_h, for continuously repeated hits, in terms of on and off time. To express this in terms of on and total times, we make the common (though debatable) assumption that k is the same for cooling and heating. Then

$$e^{-ka}e^{-kb} = e^{-k(a+b)} = e^{-kc}$$

and equation 11 becomes

$$F_h = \frac{1 - e^{-ka}}{1 - e^{-kc}} \tag{12a}$$ *

GROUP OF HITS

Let us now solve for the high temperatures, H, within a single group. (We omit low temperatures, because the high temperatures are generally used as a measure of the capacity of equipment.

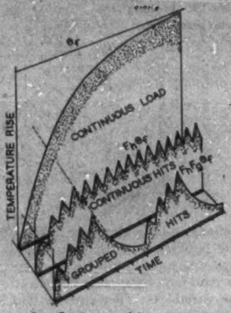


Figure 8. Cutting welds into groups has important effect on temperature rise

The difference is measured by the duty factors derived in the text

The effect actually is more marked than this drawing indicates, since to get the illustration within limited space, the "on" and "rest" times were made very long in relation to the heating time constant of the hypothetical equipment involved. This makes the temperature reach its maximum after a few hits, whereas in the numerical example given in the text the rise is so much slower that it takes six groups to reach 90 per cent of the final temperature

If at any time it becomes advisable to specify an effective temperature involving maximum and minimum values and their relative durations, the treatment should be extended to include this effect.) We will find H_n (Figure 11) by solving for each cycle in succession, starting with equation 8 to get the first cycle in terms of an arbitrary L_o, then using the relation of equation 10, with kc substituted for $k(a+b)$.

For the first hit,

$$H_1 = 1 - e^{-ka} + e^{-ka}L_o \tag{13}$$

For hit 2, from equation 10 and 13,

$$\begin{aligned} H_2 &= 1 - e^{-ka} + e^{-kc}(1 - e^{-ka} + e^{-ka}L_o) \\ &= (1 - e^{-ka})(1 + e^{-kc}) + e^{-k(a+c)}L_o \end{aligned} \tag{14}$$

For hit 3, from equations 10 and 14,

$$\begin{aligned} H_3 &= 1 - e^{-ka} + e^{-kc}[(1 - e^{-ka})(1 + e^{-kc}) + e^{-k(a+c)}L_o] \\ &= (1 - e^{-ka})[1 + e^{-kc}(1 + e^{-kc})] + e^{-k(a+2c)}L_o \\ &= (1 - e^{-ka})(1 + e^{-kc} + e^{-2kc}) + e^{-k(a+2c)}L_o \end{aligned} \tag{15}$$

By continuing this process and comparing equations 13–15 and their successors, we can write the general expression

$$H_n = (1 - e^{-ka}) \sum_{m=0}^{n-1} e^{-mkc} + L_o e^{-k[a+(n-1)c]} \tag{16}$$

This can be reduced to a more usable form by letting $a+(n-1)c = d$ (see Figure 11), and by taking advantage of the fact that summation term is a geometric series starting with unity. The sum of such a series is $(1-r^N)/(1-r)$. In this case the ratio between successive terms, is e^{-kc}; and N, the number of terms to be summed, is n. Equation 16 becomes

$$H_n = (1 - e^{-ka})(1 - e^{-knc})/(1 - e^{-kc}) + L_o e^{-kd} \tag{17}$$

CONTINUOUSLY REPEATED GROUPS

The relation that has been derived for temperature at the end of a group and that at its beginning permits us to link groups. Consider a time interval f between groups (Figure 12), and use subscripts d and $d+1$ to denote the ends of the last hit in respective groups, with g and $g+1$ marking the end of rests between groups.

Then at the end of the gth cooling interval, the low temperature is

$$L_g = H_d e^{-kf} \tag{18}$$

whereas during the active period of the next group, from equation 17,

$$H_{d+1} = (1 - e^{-ka})(1 - e^{-nkc})/(1 - e^{-kc}) + L_g e^{-kd} \tag{19}$$

If we substitute equation 18 into equation 19,

$$H_{d+1} = (1 - e^{-ka})(1 - e^{-nkc})/(1 - e^{-kc}) + H_d e^{-kd}e^{-kf} \tag{20}$$

Dynamic equilibrium comes between groups when $H_d = H_{d+1}$. Since this value is the ratio of *maximum temperature*

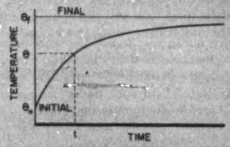


Figure 9. Exponential heating or cooling curve

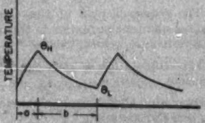


Figure 10. Hit cycle

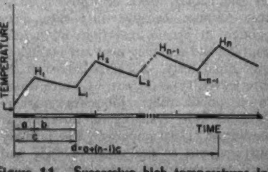


Figure 11. Successive high temperatures in groups of hits

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* If instead of on–off cycle, there is a high–low load cycle,

$$\begin{aligned} H &= \frac{1 - e^{-ka} + Pe^{-ka}(1 - e^{-kb})}{1 - e^{-ka}e^{-kb}} \\ &= \frac{1 - Pe^{-kc} - (1-P)e^{-ka}}{1 - e^{-kc}} \\ &= \frac{1 - e^{-ka} + P(e^{-ka} - e^{-kc})}{1 - e^{-kc}} \end{aligned}$$

where P is the ultimate temperature rise if the light load were applied continuously, divided by θ_f.

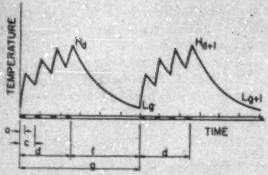
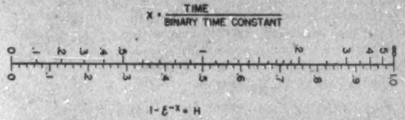


Figure 12. High and low temperatures in successive groups

Figure 13. Heating factor versus time



Values may be taken from this alignment chart, for solving equations 20 or 21

For values of x lower than 0.1, $1-e^{-x}=0.693x$

That is the range in which "duty cycle" is accurate for continuous hits

rise under intermittent load to temperature rise under continuous load, we may call it F, the *duty factor*. We find this by solving equation 20, and noting from Figure 11 that $d+f=g$:

$$F=\frac{(1-e^{-ka})(1-e^{-knc})}{(1-e^{-kc})(1-e^{-kg})}=F_hF_g \tag{21}$$

Thus we have the complete duty factor. In computation, it may be thought of as having either two factors or four. If we use two, as was done in preparing the charts of Figures 6 and 7, the first terms in numerator and denominator combine to give F_h the hit duty factor; the second terms give F_g the group duty factor. Or the four values in parentheses may be taken from Figure 13 (or, for greater accuracy, from a modern table of exponential functions) and combined to find F.

This duty factor takes the place of duty cycle in the formulas for determining the periodic load that will heat equipment to its thermal limit. Thus, for example:

$F\cdot$ Permissible watts loss under periodic load = permissible continuous loss

$\sqrt{F}\cdot$ Rated current under intermittent load = rated continuous current

Tests on Kickless Welding Cable

The duty-factor theory has been substantiated by tests that checked:

(*a*). The reasonableness of the exponential-rate-of-change assumption in the case of kickless resistance-welding cable.

(*b*). The temperature at each peak during intermittent operation.

(*c*). Dynamic equilibrium temperatures during periodic operation.

The tests were run on water-cooled welding cables, which are convenient for this purpose because they are large enough to permit insertion of thermocouples, and still they heat fast enough to give rapid tests. Kickless cables of the interleaved type (Figure 14) were tested in a public-utility laboratory, where facilities were available for intermittent loading at 21,000 amperes. The primary purpose was to find heat balance and local temperatures under various combinations of current, cable shape and constrictions, total water flow, and ratio of water between inner and outer passages. Transient and steady-state temperatures were read for a comprehensive selection of these variables. Then intermittent load was applied, several cases having long enough period to permit temperatures to be read after each hit (probably within two degrees centigrade). From these runs data were obtained for the checks previously listed.

Tests for Exponential Rate of Change

Figure 15 shows temperature–time curves for continuous load tests, with three different rates of water flow through light-duty cables. (Similar curves were obtained for heavy-duty cables, but are not needed for this discussion.) The ordinate is really logarithm of (100 per cent temperature change), but is graduated directly in terms of per cent change.

The fact that the tests produced these straight lines gives a sound basis for the use of the exponential expression in the section, "Duty Factor: Its Use and Meaning."

The binary heating time constant is read from each curve: it is the time at which the line crosses the 50 per cent temperature value. Although further tests may refine the time constants, it should be remembered that they do not radically affect temperatures under periodic load. That is, a ten per cent difference in time constant means at most a two or three per cent change in duty factor.

The data for Figure 15 were chosen because of their connection with the inter-

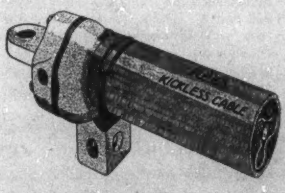


Figure 14. Light-duty Rees kickless cable upon which tests were run

The six component cables may be seen at the cut end. The three within the rubber separator are paralleled at the terminal cap, whereas the three outside the separator are paralleled in the base of the terminal

A stream of water is directed down each cable, in such volume as to equalize the temperature between inner and outer cables. Temperature difference between wire and water is minimized by small sections of copper. Continual spiraling of cables assures water cooling near all points of contact between wire and rubber

Thermocouples were placed in cables and at several points in terminals at water-outlet end of test cable. Temperatures given in other illustrations are hot-spot values

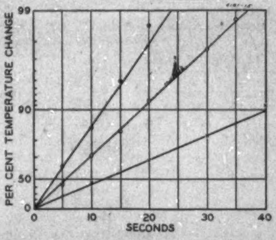


Figure 15. Heating curves for light-duty kickless cable

Each line represents a different rate of water flow through the cable

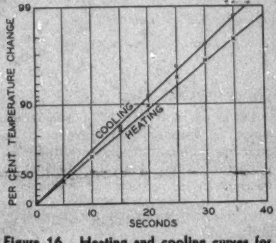


Figure 16. Heating and cooling curves for same water flow

For very accurate computations of duty factor, the difference between heating and cooling time constants may be recognized. In such a case, Figure 6 is more readily used than is Figure 7 mittent load tests, but they are representative of most results. Certain other data were not so accommodating, however, and there is some indication of curvature in the early parts of many tests. More work needs to be done on this matter, if only to determine accurate time constants for various conditions.

A point that may be important is the difference between heating time constants for heating and for cooling. In Figure 16, companion heating and cooling curves are shown. There is a noticeable difference, which seems to be consistent in all tests: cooling time constant is some 20–40 per cent less than heating This may be caused by a combination of

1. Variable losses in the cable due to increasing resistance as the wires heat.

2. The effect of difference between water temperature and copper temperature.

3. The transfer of heat along the cable as the water travels through it.

Strictly, this means that the cooling curves in Figures 8–12 should drop 20–40 per cent faster than they do.

For the present, however, this may be neglected, and the *heating* time constant may be used for both heating and cooling.

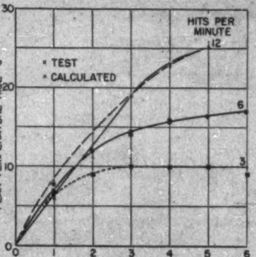


Figure 17. Test versus calculated hot-spot temperatures on light-duty Rees 267,000-circular-mil cable carrying 21,000 amperes for one-sixth second, at rates indicated on chart

Then ratings obtained for a given duty cycle will be conservative, since the calculation will not take account of the faster rate of cooling.

Peak Temperatures at Start of Periodic Heating

In one set of tests on a light-duty cable, hits of 21,000 amperes $^{1}/_{6}$-second duration were applied at the rate of 3, 6, and 12 per minute. In each case the temperature rose and fell according to the general pattern of Figure 11: and the high temperatures after each hit should follow equation 17. Test and calculated temperatures are compared in Figure 17.

Similar results were obtained when the rate of hits was changed without intermission, from three to six hits per minute.

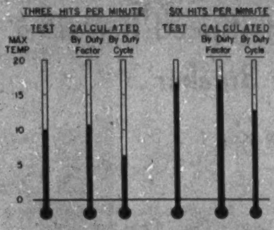


Figure 18. Comparison of test and calculated temperatures on kickless cable carrying 21,000 amperes, current "on" time one-sixth second, at welding rates noted

Dynamic-Equilibrium Temperatures

Of the three tests discussed, two were continued until successive peaks had equal values. In Table I these tests are compared with temperature rises calculated by both duty-factor and duty-cycle methods. (The theoretical temperature rise for 21,000 amperes, found by methods beyond the scope of this paper, is 760 degrees centigrade.)

Since maximum averaging time for the cable is not known, duty-cycle computations were made for one hit cycle. If the averaging time were one-sixth second longer, the temperatures in the last column would be practically doubled, emphasizing the arbitrary nature of duty cycle and averaging time.

Figure 18 shows the tabulated results graphically. The agreement between test and duty-factor results is in marked contrast to the discrepancy found in "duty cycle."

Conclusion

By means of the simple charts derived from the duty-factor method of rating under periodic load, savings can be made:

(*a*). In designing new equipment.

(*b*). In applying new loads to existing equipment.

This is in line with engineering trends to load everything to full capacity, but not beyond.

The accuracy and easy application of duty-factor method recommend it for adoption, in conjunction with the heating time constant, as a standard method of rating equipment subject to intermittent loads.

Appendix

To use this duty-factor principle on other equipment, it is necessary to know the heating constants. Little has been published on this subject. The following have been taken principally from the papers listed in reference 4:

Power Transformers[4a]

Kilovolts	Megavolt-Amperes	Time Constant (Binary) (Hours)
13	1–5	1.7
34	8–20	1.9
69	1–2.5	3.0
69	5–33	2.1
110	5	2.6
	33	2.2
138	2.5–20	3.0
	33	2.5

Motors

One horsepower—23 minutes[4b]

One-half- to five-horsepower general-purpose induction—for heating 7 minutes—for cooling 35 minutes.[4c]

Thirty horsepower, three-phase six-pole induction, 500 rpm, 440 volts, 25 cycles—30 minutes.[5]

Fifty horsepower, direct current, four pole, 1,075 rpm, 230 volts—21 minutes.[5]

Ignitron

Small water-cooled ignitron[10]—16 seconds.

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Tests of 230-Kv High-Speed Reclosing Oil Circuit Breaker

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MEMBER AIEE

Synopsis: The advantages of high-speed reclosing of transmission lines in 20 cycles or less are well established for voltages up to 138 kv. Up to this date little information is available however on reclosing 230 kv lines in 20 cycles or less, because breakers have not been available for such fast operation. This paper describes a 230-kv oil circuit breaker which is suitable for three-pole or single-pole reclosing with a reclosing time of less than 20 cycles. The performance of the breaker is established by field tests for high-speed-reclosing operation as well as for interruption of charging currents.

IN the late 1920's, the increasing demand for fast-clearing circuit breakers brought forth the development of various types of modern arc-rupturing devices. Among others, there was developed the expulsion-chamber arc-rupturing device[1] which now is in satisfactory operation in thousands of breakers. The theory of expulsion action and the performance of the expulsion-chamber breakers have been reported on frequently. Test on a 115-kv system showed arcing times of approximately $1^1/_2$ cycles at short-circuit capacities ranging up to 1,650,000 kva.[1] Tests on a 230-kv system showed arcing times of approximately 2.5 cycles at ratings up to 1,125,000 kva.[2] In the middle 1930's the practice of high-speed reclosing was introduced and is now widely accepted. Interrupting capacity tests performed on a 69-kv system showed that the expulsion-chamber arc-rupturing devices gave excellent performance in conjunction with high-speed-reclosing operation.[3] Following the demand for still shorter opening time, more recently five-cycle breakers have come into wide use. On highest voltages, particularly in conjunction with switching of long lines, the use of three-cycle breakers frequently is indicated.

Finally the method of single-pole reclosing on highest voltages in recent years has attracted considerable attention. Because of the wide distribution of 230-kv systems, it was therefore decided to subject a modern five-cycle high-speed-reclosing 230-kv circuit breaker to field tests, to analyze its performance generally and in particular with regard to the possibilities of developing a three-cycle breaker. Furthermore, it was hoped that the data would enable the drawing of conclusions as to a decrease in tank diameter, oil volume, and resulting reduction in cost. Through the courtesy of the Bonneville Power Administration, the North Bonneville Substation and four generators of the Bonneville power house were made available for these tests.

Description of Breaker, Operating Mechanism, and Test Connections

BREAKER

The breaker tested consisted of a single-pole unit of a 196/230-kv circuit breaker with its high-speed-reclosing mechanism. Figure 1 shows a cross section through the breaker, which consists of six expulsion chambers arranged in series supported by the bushings. The movable blades are fastened to a vertically rotating bakelite tube. These blades move at speeds up to 28 feet per second, and therefore rapidly introduce a large break distance. The expulsion chamber resembles an explosion chamber; however, it radically differs in its action in that only moderate pressures are developed because of the provision of an expulsion port to the rear of the chamber. As previously explained,[1] the gases are accumulated to produce a more effective gas blast during the period of interruption. The oil volume within the chamber is kept small in order to prevent interference of large bodies of oil with the free escape of gases. However, an amount sufficient for multiple interruptions is provided. Figure 2 shows an expulsion chamber during the various stages of breaker opening.

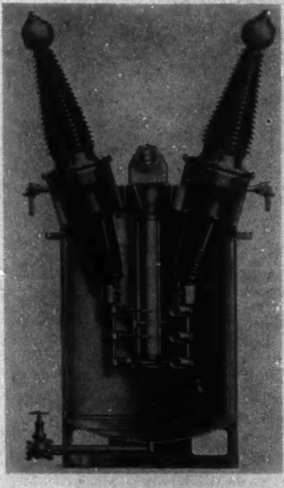
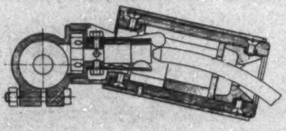


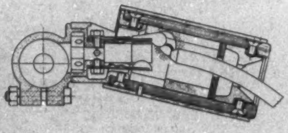
Figure 1. Cross-sectional view of the expulsion oil circuit breaker

OPERATING MECHANISM

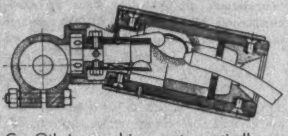
The operating mechanism is of the motor-wound spring-actuated trip-free type; its operating linkage corresponds to that of a solenoid control in which the closing coil and plunger are replaced by a prewound closing spring. When the



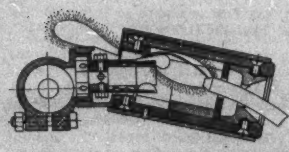
A. Contacts separate



B. Gas accumulating. No gas-blast action



C. Oil in expulsion port practically removed. No gas-blast action



D. Expulsion port cleared. High-velocity gas blast through the arc

Figure 2. Operation of expulsion chamber

Paper 44-192, recommended by the AIEE committee on protective devices for presentation at the AIEE Los Angeles technical meeting, Los Angeles, Calif., August 29–September 1, 1944. Manuscript submitted June 1, 1944; made available for printing July 11, 1944.

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The author acknowledges the assistance of the engineering and operations departments of the Bonneville Power Administration in the performance of the tests described in this paper.

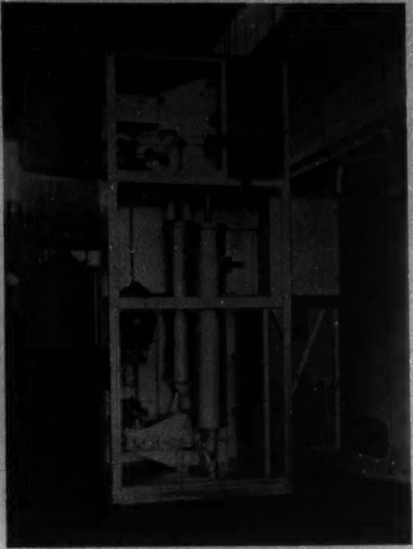

Figure 3. Operating mechanism

breaker is in the closed position, the closing spring also is wound and in case of opening of the breaker is available immediately to initiate the reclosing operation. Figure 3 shows the control applying to the breaker under test, the largest one of a line ranging from 7.5 to 230 kv. Figure 4 is a schematic diagram of the sequence of operation. The mechanism power is transmitted through a horizontal rotating shaft to the individual breaker poles and in each is transformed into the vertical breaker-shaft motion by means of a bevel link, which provides a toggle in the closed position, assuring perfect contact at all times.

During the opening or closing operation each blade provides a break of $24^{3}/_{4}$ inches, the total with six breaks amounting to $148^{1}/_{2}$ inches. During the reclosing operation the blades open to the full distance of $24^{3}/_{4}$ inches each, insuring reliable and unimpaired performance during the first opening of the series. This feature is made possible by the high speed of blade travel. In order to obtain data concerning the required blade speeds for five-cycle and possibly three-cycle breakers, the test-breaker operating mechanism was provided with various spring combinations, giving opening velocities of from 18 to 28 feet per second. When connected to a three-pole breaker, the opening time (trip coil energized to arcing tips separate) amounts to 2.5 cycles or less; however, because only one pole was tested, a reduction in initial spring pressure was made, increasing the opening time to approximately three cycles. The reason for this is that the accelerating forces upon the trip levers also are reduced, increasing the time for the blades to proceed to the stationary-contact arcing tips. This deviation from three-pole oil-circuit-breaker conditions was of no consequence since the primary purpose of the test was to obtain arcing-time data. Opening times are determined readily by factory tests.

Test Setup

System connections and power sources

1. Single-phase short-circuit-to-ground opening and reclosing tests: two or four 60,000-kva generators feeding through transformers of corresponding kilovolt-amperes over one mile of 230-kv line. Voltage-recovery rates regulated by switching on or off 133 miles of 230-kv idle line.

2. Interruptions of line-charging-current tests: The 230-kv system charging 133 miles of 230-kv line. Tests were single pole, the other two phases being opened a few cycles before, simultaneously, or a few cycles after the test oil circuit breaker.

Equipment

A seven-element magnetic oscillograph was used to record all 60-cycle data, and the oil-circuit-breaker arc-tip position and opening travel. The arc-tip indicating contact was mounted directly on the oil-circuit-breaker rotating unit, eliminating any backlash and having entirely negligible deflection due to inertia effects. Switching voltages were measured by two methods. A sphere gap connected through a capacitance divider to the bushing capacitance tap, or a cathode-ray oscillograph similarly connected and fed through a concentric line. The latter was calibrated by over-all tests between one megacycle and 60 cycles and showed negligible distortion and attenuation in this range.

Breaker Tests

Tests were conducted during December 1943 and January 1944. Table I gives data on open and high-speed open–close–open shots.

Tests 1, 2, and 3 establish an arcing time of 1.8 cycles maximum for current values of around 870 amperes. Since the breaker rating of 2,500,000 kva corresponds to a rated interrupting current of 6,300 amperes, it can be seen that the breaker is capable of interrupting short-circuit currents considerably below 25 per cent maximum rating with an average arcing time of but 1.2 cycles.

Tests 13, 14, and 26 establish a maximum arcing time of 1.4 cycles for cur-

Table I. Short-Circuit Open and High-Speed Open–Close–Open Tests

Test Number December 19, 20, 1943	Duty Cycle	Kv to Ground Before Test; Phase C	Number of 60,000-Kva Generators	133 Miles of 230-Kv Line	Opening Speed, Ft Per Sec	Rms Amperes Interrupted	Total Time to Clear	Arcing Time	De-energized Time	Kva Interrupted, Equivalent 3 Phase	Cold Gas, Generated Gallons
1	O	143	2	On	28	850	3.9	1.0		364,000	No record
2	O	144	2	On	28	870	4.7	1.8		375,000	O
3	O	144	2	On	28	870	3.5	0.7		375,000	O
13	O	134	4	Off	28	1,910	4.2	1.4		766,000	$1^{1}/_{2}$
14	O	133	4	Off	28	1,860	4.1	1.2		742,000	$^{1}/_{2}$
26	OCO 1st opening	136	4	Off	23	1,900	4.5	1.4		775,000	$1^{1}/_{2}$
	OCO 2d opening		4	Off	23	1,970		0.5	12.6	805,000	

All times are in cycles at 60 per second. O—Open. OCO—Open-close-open.

Table II. Single-Phase Line-Charging-Current Interruption Tests

Line Kilovolts to Ground 127 to 130 Rms Amperes from 70 to 105

Test Number and Date	Opening Speed, Ft Per Sec	Resistors Across Expulsion Chambers	Backup OCB Phase A and B Open Before or After Phase C of Test OCB, Cycles	Cycles of Arc	Number of Restrikes	Ratio of Phase C Maximum Voltage to Normal Crest—Does Not Exceed	
						By Cathode-Ray Oscillograph	By Spark Gap
December 19, 1943							
15	28	No	0.2 before	1.0	1		
16	28	No	8.8 after	0.9	1		
17	28	No	10.1 before	1.2	1		
18	28	No	11.3 before	0.14	0		
19	28	No	11.2 before	1.1	1		
20	24	No	0.7 before	1.5	2		
21	24	No	1.3 before	1.2	1		
23	24	No	3.4 before	1.1	1		
24	24	No	1.7 before	1.2	1		
25	24	No	0.7 before	1.0	1		
January 9, 1944						Bus Side	Line Side
1	28	Yes	0.0 before	1.9*	0	1.2	1.36
2	28	Yes	1.4 before	2.1*	2	1.2	1.36
3	28	Yes	0.8 before	2.6*	0	1.2	1.36
4	21.5	Yes	0.5 before	3.0*	0	1.2	1.36
5	21.5	Yes	0.5 before	2.9*	0	1.2	1.36
6	21.5	Yes	1.1 before	2.2*	0	1.2	1.36
7	18	Yes	3.7 before	2.8*	0	1.2	1.36
9	18	Yes	3.4 before	3.1*	1	1.2	1.36
10	18	Yes	3.6 before	3.3*	2	1.2	1.36
11	28	No	7.0 after	1.14	1	1.1–1.2	1.36
12	28	No	7.1 after	0.8	1	1.1–1.2	1.36
13	18	No	5.3 after	2.0	3	1.1–1.2	1.36
14	18	No	5.0 after	1.1	1	1.1–1.2	1.36
15	18	No	5.6 after	1.4	2	1.1–1.2	1.36
						Line Side	
16	18	No	4.6 after	1.2	3	1.55	Discontinued

All times are in cycles at 60 per second. * Arc tips part to final clearing. OCB—Oil circuit breaker.

rents above 25 per cent of full breaker rating for operating speeds as low as 23 feet per second. Since no lines were connected during these tests, the rates of rise of recovery voltage were high, and the tests represent a condition more severe than can be expected during normal operating conditions. The maximum current interrupted is considerably below the rated interrupting current; however, previous published tests on a 115-kv system up to 10,000 amperes have established the fact that the arcing time decreases with increasing current. This decrease in arcing time is due to the fact that higher currents generate gases at an increased rate, producing a more effective gas-blast action. Important loads precluded the possibility of tests including the entire system. However, since the performance of the expulsion chambers at higher currents is so well established, it may be assumed that such tests could not but conform.

Test 26 represented by oscillogram, Figure 5, gives performance under high-speed-reclosing conditions. The reclosing time amounted to 17 cycles, the de-energized time to 13 cycles. The first interruption occurred with an arcing time of 1.4 cycles, the second one with an arcing time of only one-half cycle.

Much effort has been spent in recent years to produce rupturing devices for high-speed-reclosing oil circuit breakers with the purpose of reducing the arcing time of the second interruption to that of the first one. In considering the expulsion chambers, it could be assumed that a definite amount of time is required for the chamber to recover its interrupting ability. However, since these chambers are operating on the gas-blast principle and require but a small amount of oil for each interruption, and since a liberal amount of oil is present in the expulsion chamber, it can be anticipated that the chamber requires no recovery time at all. This assumption is verified by the short arcing time of only one-half cycle during the second opening.

During all the aforementioned tests, the vent pipe was completely closed off by a deflated paper bag. The amount of inflation of this bag represents a simple means of approximating the volume of gases produced during each interruption.

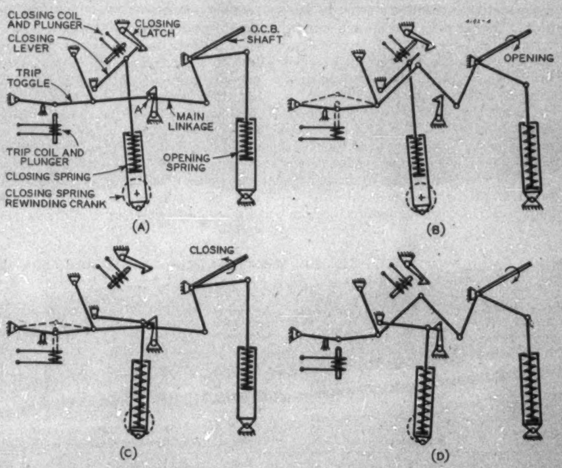

Figure 4. Operating-mechanism linkage

A. Breaker closed
Closing spring and opening spring charged, ready for trip, immediate reclose, and second trip cycle

B. First opening
Trip-coil plunger trips trip toggle which starts resetting as soon as pin *A* of main linkage has cleared prop. Opening spring opens breaker and brings pin *A* into vicinity of closing lever which is released by means of closing-coil plunger

C. Closing
Upon release of closing lever (Figure 4B), breaker is closed and prop engages pin *A*. Trip coil is again energized and plunger breaks opening link which resets as soon as pin *A* has cleared prop and pawl on closing lever

D. Second opening
Breaker opens as in Figure 4B, and motor limit switch energized turning crank one revolution, resetting closing lever on closing latch and charging spring

When breaker is closed and remains closed, motor again turns crank one revolution, bringing control to original position, Figure 4A, ready for immediate-reclosing cycle

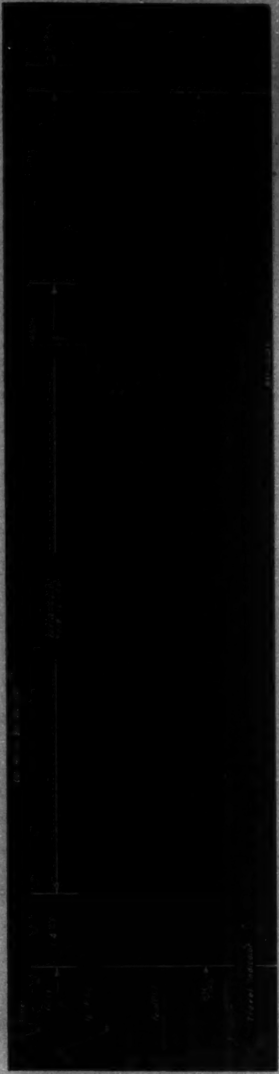

Figure 5. Magnetic oscillogram of reclosing test 26, December 20, 1943

Figure 6. Magnetic oscillogram of charging-current interruption, test 15, December 19, 1943

As can be seen from the last column of Table I, the maximum amount was $1^1/_2$ gallons. The inflation of this paper bag was the only visible evidence of the interruption; no disturbance was noticeable during any of these tests.

Charging-Current-Interruption Tests

During these tests, one phase of a three-phase line charged by the substation bus was opened by the test breaker; the other two phases were opened a few cycles before, simultaneously, or a few cycles after by a station breaker.

It has been recognized for a long time that the interruption of charging currents of long transmission lines represents the severest duty that can be imposed upon a circuit breaker.[4] The theory of restrikes and the subsequent building up of high overvoltages has been reported upon in detail[5] and requires no further discussion here. Recent investigations show that breakers equipped with most modern arc-rupturing devices can produce overvoltages in excess of $4^1/_2$ times normal voltage, because of multiple restrikes, and that these voltages can be reduced to harmless values of twice normal voltage by the provision of resistances across the interrupting devices.[6]

Accordingly, parallel resistances of 500 ohms were built for installation across each of the six expulsion chambers. Tests were performed with and without resistances; in addition, the opening speed was varied. Table II gives the results of the charging-current-interruption tests.

Of interest is the very short arcing time without use of resistances and its increase with decrease in opening speed. At 28 feet per second speed, without resistances the number of restrikes was limited to one, at 24 feet per second two restrikes occur in one case out of 5, and at 18 feet per second the maximum number of restrikes amounts to 3. Resistances eliminate the restrikes except for small suppressed restrikes, but result in an increase in effective arcing time to values as high as 3.3 cycles against a maximum time without resistances of two cycles. Voltage measurements by cathode-ray oscillograph and spark gap show a maximum possible value of 1.36 times normal voltage. No voltage records were taken during the December 19 test; however, since the highest voltages are likely to occur at the lowest operating speeds and corresponding maximum number of restrikes, it is felt that the omission is not important. The behavior of the expulsion chamber in interrupting charging currents in such short times without the use of resistances or other auxiliary devices seems to justify a detailed study of the causes of this unusual performance.

Considering the mechanism of generation of overvoltages by the restrikes, it may be proved that to prevent such overvoltages the reignition voltage should be less than the normal line-to-ground voltage. Since the voltage builds up to the normal value within one-quarter cycle, the current-zero period should be one-quarter cycle or less. Only those restrikes in which the current-zero interval exceeds one-quarter cycle will cause overvoltages. Numerous successive delayed restrikes with current-zero intervals of less than one-quarter cycle are therefore harmless, as is evidenced by operating experience on numerous plain-break breakers known not to produce harmful overvoltages.

An arc-rupturing device therefore should have a very low dielectric-recovery rate, permitting restrikes within less than one-quarter cycle after current interruption until such time that the device is capable of withstanding full voltage. Since in the gas accumulation period, the expulsion chamber constitutes a plain-break switch and since in the low-current range this period amounts to approximately one cycle, an early and harmless restrike can be expected. After this period the gas-blast action starts and causes final interruption of the arc at the next current zero.

Figure 6 shows an oscillogram of the interruption corresponding to test 15, December 19, at a speed of 28 feet per second. A restrike occurs in less than one-quarter cycle after the first current zero to be followed by final interruption. Similar performance with not more than one restrike was obtained on all interruptions with a speed of 28 feet per second. Opening speeds of 24 feet per second as shown from test 20, December 19, can produce two restrikes of which the second one can be preceded by a zero-current interval in excess of one-quarter cycle. However, even if the delay amounts to the critical one-half cycle period, the theoretical voltage cannot exceed three times normal line voltage. Actual voltage measurements during operations with opening speeds as low as 18 feet per second and a maximum of three restrikes did not indicate any appreciable overvoltage.

The expulsion-chamber breaker, when operated with speeds above 24 feet per second, interrupts charging currents without producing overvoltages. Operation at lower speeds, although theoretically capable of producing overvoltages of three times normal, does not produce harmful values.

Conclusions

1. The expulsion-chamber rupturing devices are capable of interrupting short circuits on a 230-kv system in less than 1.5 cycles, making expulsion oil circuit breakers having an interrupting time of considerably less than five cycles a possibility.

2. Successive interruptions during high-speed-reclosing operations with only 13 cycles de-energized time are handled without difficulty by the expulsion chamber.

3. The expulsion chambers are suited ideally for the interruption of charging currents. The arcing times are small, and no harmful overvoltages are produced. Parallel resistances can be applied to expulsion chambers; however, they produce no improvement in performance.

4. The motor-wound spring-actuated operating mechanism produces reclosing speeds not heretofore reached and permits full opening of the breaker during all reclosing operations.

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Operation of Low-Pressure Gas-Filled Cable

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MEMBER AIEE

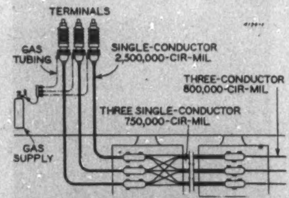
SINCE 1938 low-pressure gas-filled cable has been the source of considerable discussion before meetings of the Institute and other technical associations. Papers presented by Shanklin in 1939[1] and 1942[2] and an unpublished paper by Del Mar and Gambitta in 1943 discussed the theoretical aspects of a cable utilizing gas as pressure medium and presented extensive laboratory test data on the expected life of this type of cable. From these data it was indicated that if low-pressure gas-filled cable is operated at an average stress of less than 75 volts per mil and at a minimum gas pressure of ten pounds per square inch satisfactory operation may be expected.

Theories and laboratory test data are not always sufficient to establish a fact and they should be substantiated by actual field data whenever possible to prove the theory. It is intended in this paper to present factual data on the actual operation of low-pressure gas-filled cable of various types on the system of the Consolidated Edison Company of New York, Inc. Operating experience data in the report are divided into (1) sheath experience and (2) electrical quality of the insulation of cable removed from service.

History of Installation

In view of the satisfactory operation of oil-filled feeders installed in 1927 and 1929 where for the first time complete sheath supervision was obtained, it seemed apparent that if supervision could be maintained over the lead sheaths of solid-type cables a large percentage of electrical failures could be eliminated. The low-pressure gas-filled cable described by Shanklin in his 1939[1] paper appeared to offer a satisfactory solution to this problem of sheath supervision, for feeders operating at medium voltages, such as 15,000 volts and 27,000 volts and in addition appeared to provide greater stability against ionization. With this type of cable, positive pressure could be maintained within the cable thereby reducing the possibility of water entering the cable in the event of sheath or joint defects and providing a means of indicating when sheath or joint defects occurred.

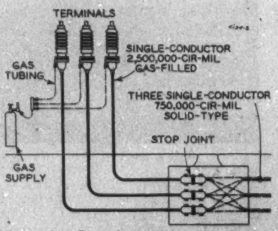
In order to obtain information on actual field operating conditions there was installed on the Consolidated Edison system during the year 1938 approximately 20,000 feet of three-conductor 800,000-circular-mil 15,000-volt low-pressure gas-filled cable. The operation of the initial 1938 installation was followed closely in the field and the results obtained appeared to check factory laboratory results. To obtain additional information on this low-pressure gas-filled cable at



TERMINALS
GAS TUBING
SINGLE-CONDUCTOR 2,500,000-CIR-MIL
THREE-CONDUCTOR 800,000-CIR-MIL
THREE SINGLE-CONDUCTOR 750,000-CIR-MIL
GAS SUPPLY

Figure 1. Typical installation of gas-filled cable at generating stations

Connection of single-conductor to three-conductor cable



TERMINALS
GAS TUBING
SINGLE-CONDUCTOR 2,500,000-CIR-MIL GAS-FILLED
THREE SINGLE-CONDUCTOR 750,000-CIR-MIL SOLID-TYPE
STOP JOINT
GAS SUPPLY

Figure 2. Typical installation of gas-filled cable at generating stations

Connection of gas-filled to solid-type cable

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The author wishes to acknowledge the help of the following: The over-all guidance of C. W. Franklin in fostering the principle of this type of system; the assistance of G. B. Shanklin and V. A. Sheals in conducting tests on the withdrawn cable; the help of F. P. West and R. W. Gillette in the assembling of the data.

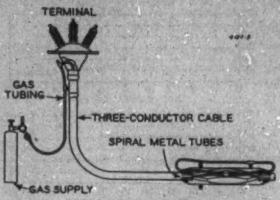


Figure 3. Connection of solid-wall tubing to joints and terminal

higher operating voltages, there was installed during 1939 approximately 8,000 feet of three-conductor 500,000 circular-mil 27,000-volt cable.

The construction program for the year 1940 involved extensive feeder rearrangements around several generating stations and the installation of additional 27,000-volt feeders between generating stations. The information which had been obtained from the 15,000-volt cable installed during 1938 and the 27,000-volt cable installed during 1939 indicated that the low-pressure gas-filled cable would be a desirable type of cable for the new cables required at the generating stations as it was desired to obtain cables where supervision could be maintained over the lead sheaths. The cables required for station lengths at the generating stations were of the single-conductor type so as to obtain phase isolation in accordance with our standard practice in generating stations. The cable manufacturers were approached at this time as to the possibility of obtaining single-conductor low-pressure gas-filled cables as all of the past experience had been obtained on three-conductor gas-filled cable. Agreement was reached on the type of single-conductor cable to be used for low-pressure gas-filled cables and our requirements for the feeder rearrangements at the generating stations were purchased as low-pressure gas-filled cable.

All of the low-pressure gas-filled cable installed on the system between 1938 and 1940 was in relatively short runs, the maximum length being approximately two miles. Runs of this length presented no serious difficulty in maintaining proper gas pressure along the entire run of cable with one point of supply. In 1941 it was decided to install a feeder from one of our generating stations to a substation approximately six miles away. Three-conductor 800,000-circular-mil 15,000-volt gas-filled cable was purchased and installed for this feeder and a single point of supply was provided for maintenance of gas pressure.

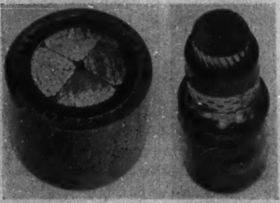


Figure 4. Provision of gas space in single-conductor cable by use of fluted lead sheath or button-embossed spacer tape

Table I. Cable Operating Under Nitrogen Pressure (13 Pounds Per Square Inch) on System of Consolidated Edison Company of New York, Inc., and Affiliated Companies

Cable Feet as of June 1, 1944

Number of Conductors	Size, Thousands of Circular Mils	Rated Voltage 27 Kv	Rated Voltage 15 Kv	Total
1	750		1,100	1,100
1	1,000	9,650	30,000	39,650
1	2,500	1,500	7,400	8,900
3	500	38,625		38,625
3	800		49,800	49,800
		49,775	88,300	138,075

Types of Cable and Cable Arrangements

As of June 1, 1944, the total amount of low-pressure gas-filled cable installed on the system is as shown in Table I.

As indicated in Table I several sizes and types of cables have been used. In general it has been necessary to use several different sizes of single-conductor cable at generating stations in order to make optimum use of existing ducts, to obtain space for terminations, and to utilize existing solid-type cables. No difficulty has been experienced in using gas-filled cable for paralleling lengths of cable, connecting a large single-conductor cable to several smaller cables, or connecting to solid cables. Typical installations of gas-filled cable at generating stations and connections to solid cable are shown on Figures 1 and 2.

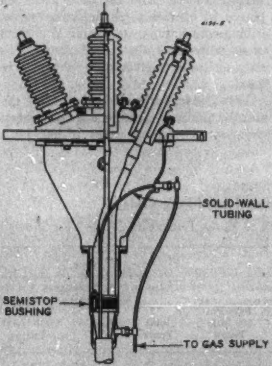


Figure 5. Three-conductor compound-filled terminal for use on gas-filled cable

Method of Installation

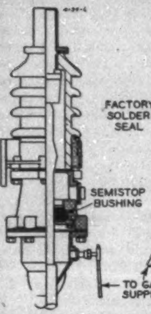
The three-conductor cable which was installed during 1938 was manufactured with three spiral tubes in the interstices. Due to oil drainage from the paper insulation, oil slugs were formed in the spiral tubes causing partial blockage of the channel spaces and preventing free flow of the nitrogen gas along the entire length of the feeder from the one point source of supply. On all subsequent three-conductor cable which has been purchased, a solid-wall copper tube for one of the three gas channels has been specified. This solid-wall tube is connected into each joint in a manner which prevents the entrance of cable compound into the tube and supplies a point of feed to the cable as shown by Figure 3. This method provides a gas supply manifold along the entire route of a feeder since the source of gas supply, namely, a cylinder of nitrogen, is connected to a terminal or joint where the solid-wall tube ends. This type of construction provides continuous supply of gas to every individual joint and tends to maintain uniform pressure along the line.

In single-conductor gas-filled cable, channels for the flow of gas were obtained by two methods: (1) the application of a button-embossed copper tape between the cable core and the lead sheath, and (2) by the use of fluted lead sheath. Figure 4 shows the two types of construction of single-conductor cable.

At the time of the installation of the original gas-filled cables at the generating stations, gas-filled terminals were not available. Development work had not progressed to the point where manufacturers were willing to guarantee clearances within the terminals with only nitrogen gas as a filling medium. In order to carry the gas-filled cable into

Table II. Classification of Leaks

	Total Number Installed	Number of Leaks
Terminals	350	30
Relays	24	8
Fittings		19
Normal joints	581	8
Stop joints	46	0
Gas-supply equipment	24	30
Cable sections	759	4
Cable adjacent to joint		46
Not located		18
Total leaks		163



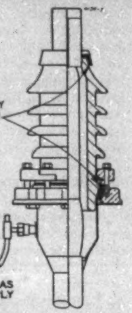


Figure 6 (left). Single-conductor compound-filled terminal for use on gas-filled cable

Figure 7 (right). Single-conductor gas-filled solder-seal terminal for use on gas-filled cable

terminals a standard-type terminal was used with a semistop feature incorporated in the wiping sleeve of the terminal. The semistop feature was installed to prevent the filling compound within the terminal from flowing into the gas-filled cable. This semistop feature did not in all cases prevent the gas from entering the terminal after it had been completely installed. All of the original terminals which were used were of the normal gasketed type. Figures 5 and 6 show the terminals used for three-conductor and single-conductor cable with the semistop features which were incorporated in the wiping sleeves. Considerable difficulty was experienced with leaks at the gaskets in these terminals. Terminals which were initially tight developed leaks after the gaskets had been subjected to working when the cables were loaded. In order to eliminate this source of trouble development work was continued by the manufacturers on gas-filled terminals with the final result that there became available gas-filled solder-seal-type terminals which eliminated all gaskets. On recent installations of single-conductor gas-filled cable at stations, this type of terminal has been used. Figure 7 shows such a gas-filled terminal.

Types of Installations

There are now in service on the Consolidated Edison system 21 feeders which are composed in part of gas-filled cable. The feeder which includes the longest continuous run of gas-filled cable is one which operates at 13.2 kv and is composed of three-conductor 800,000-circular-mil cable extending a distance of approximately 33,000 feet from a point near the Sherman Creek generating station to the Columbus Avenue substation of The Yonkers Electric Light and Power Company in Yonkers, N. Y.

Another feeder 7,500 feet in route length is composed of three legs of 27-kv cable from the East River generating station to a submarine crossing across the East River. There are also two three-conductor 27-kv feeders approximately 5,600 feet in length which extend from the Hell Gate generating station to the Queens district. These two feeders are installed through a tunnel underneath the East River and have vertical riser lengths of approximately 250 feet at each end. Most of the remaining 17 feeders have relatively short sections of gas-filled cable adjacent to the stations which is predominantly single-conductor cable. Because of this use as station legs there is a considerable number of terminals in proportion to the length of cable involved. Figure 8 shows a typical profile of one of the feeders and Figure 9 shows the profile of a feeder through a tunnel.

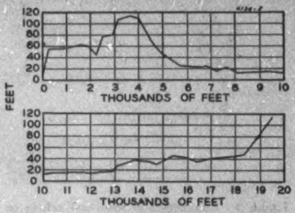


Figure 8. Portion of profile of three-conductor 800,000-circular-mil 13.2-kv gas-filled feeder

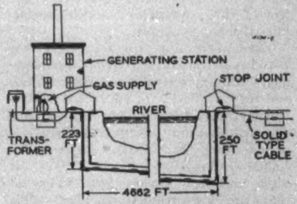


Figure 9. Profile of three-conductor 500,000-circular-mil 27-kv gas-filled feeder installed in tunnel under East River

Operation

Mechanical Trouble

Gas leaks have developed from time to time in the cable joints and terminals. In general, with one outstanding exception, it can be said that leaks have developed shortly after the cable has been placed in operation, as defective wipes, tubing connections, and terminal gaskets or seals are most apt to begin leaking as soon as cable movement occurs due to loading.

Very fast leaks due to bull-pointing the sheaths, and very minute leaks have been experienced. In all cases it has been possible to maintain gas pressure until the leak has been found and repaired.

The approximate location of leaks is normally determined by the pressure-drop method; that is, a fixed pressure is maintained at both ends of a known run of cable and pressure is measured at the intervening joints. The point of lowest pressure is the approximate location of the leak. The application of liquid soap to all exposed parts of the system at this location will then reveal the actual point of leakage if it is outside of the conduit run. In order to reduce the time required to locate a leak on a long run of three-conductor cable, gas-flow direction indicators are permanently installed at specified intervals so that the direction of gas flow past these points can be determined.

The indicator consists of two glass jars, each filled half-way with a light cable oil and sealed gas tight with a metal cover

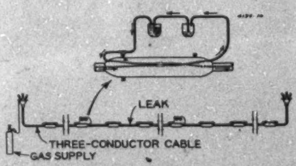


Figure 10. Installation of gas-flow direction indicators on three-conductor cable for location of gas leaks

Table III. Operating Record of Solid-Type Cable

Three-Conductor and Single-Conductor, 15-Kv to 33-Kv, Two Years Old in Year Shown

	Cable					Joints		
		Failures		Removals			Failures	
Year	Mile Years	Number	Rate Per 100 Mile Years	Number	Rate Per 100 Mile Years	Joint Years	Number	Rate Per 1,000 Joint Years
1940	170	13	7.7	1	0.6	4,942	9	1.8
1941	160	11	6.9	0	0	4,428	10	2.3
1942	253	9	3.6	1	0.4	7,522	7	0.9

Table IV. Operating Record of Solid-Type and Gas-Filled Cable Combined

Three-Conductor and Single-Conductor, 15-Kv to 33-Kv, Two Years Old in Year Shown (Assuming Failures Had Resulted From One-Half the Gas Leaks)

	Cable					Joints		
		Failures		Removals			Failures	
Year	Mile-Years	Number	Rate Per 100 Mile-Years	Number	Rate Per 100 Mile-Years	Joint-Years	Number	Rate Per 1,000 Joint-Years
1941	161	12	7.5	0	0.0	4,454	11	2.5
1942	263	11	4.2	1	0.4	7,798	9	1.2

and gasket. The two jars are connected in series and are inserted in the solid wall gas tube of the cable at a joint location. Under normal conditions when there is no flow of gas in the cable the oil in the two glass jars remains at the same level. When there is a flow of gas due to a leak gas will flow from the supply tank into the indicator forcing all of the oil into the glass in the direction of the leak. The direction of gas flow is also indicated by bubbles rising to the surface of the oil in the jar in the direction of the leak. The indicator and a typical installation are shown in Figure 10. With this method a leak can very readily be located between two indicator locations and the pressure-drop and liquid-soap method used for definite location within the section.

On the installations of parallel lengths of single-conductor cable at generating stations the gas supply from a common tank to individual cables has been carried through valves. When a leak is indicated by loss of gas from the tank the particular length of cable which is leaking can be determined by operation of the valves.

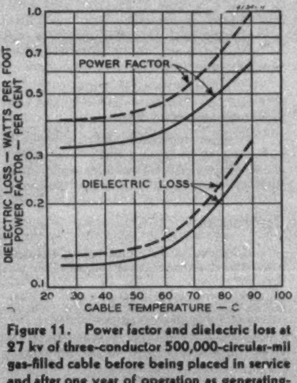


Figure 11. Power factor and dielectric loss at 27 kv of three-conductor 500,000-circular-mil gas-filled cable before being placed in service and after one year of operation as generating-station tie feeder

Ten pounds per square inch nitrogen pressure, 225-mil insulation

Solid curves—Before placing in service

Dashed curves—After one year in service

In very serious cases of sheath rupture it has been possible to maintain gas pressure and continue operation of a feeder. The 13.2-kv Sherman Creek feeder previously referred to as being the longest run of gas-filled cable was placed in service in October 1941. Prior to 1943 due to system operating conditions it carried little or no load. During 1943 it became necessary to load the cable to its normal load for approximately 20 hours each day with very light loads for the remaining 4 hours. With this load cycle, movement of cable in the manhole occurred and due to cable arrangement in the manhole the sheath was buckled and cracked.

Subsequent to finding the first sheath breaks a survey was made of the manholes and it was found that in 74 per cent of the manholes inspected the cable sheath was buckled adjacent to the joint wipe. Breaks have occurred in these buckles in approximately one-fourth of the manholes. The same sheath failures would have occurred in solid cable of the same conductor size installed under the same conditions and would without doubt have been responsible for a number of electrical failures.

The leaks have been found by the methods previously described and repairs have been made by patching the sheath before any known water was admitted.

A summary of all leaks which have occurred on all of the gas-filled cable installed on the system is shown in Table II. The types of leaks which have developed do not indicate that any particular type of cable is more or less susceptible to trouble than any other type.

Of the four leaks noted in the table as being in cable sections, one was caused by bull-pointing and was repaired in place without removing the cable; another necessitated cutting the cable at the point of leakage and replacing a short section of cable, while the remaining two leaks which were caused by foreign matter in the ducts required the replacement of the sections.

ELECTRICAL TROUBLE

Only one electrical trouble has occurred on the gas-filled cable. This fault was a test failure on a single-conductor 15,000-volt cable which had been severely damaged during installation. Examination of the fault indicated that approximately half of the paper tapes had been broken and torn by a severe twist and failure occurred at the point of broken tapes when a proof test was applied prior to placing in service.

No service failures have occurred in any of the 138,000 feet of gas-filled cable installed on the system.

That electrical failures could have been expected on this amount of cable, had it been of the solid type instead of gas filled, is indicated by the data contained in Table III. This table gives the failure rates in 1940, 1941, and 1942 of solid-type cable of comparable voltage rating which was two years old in those years.

For purposes of comparison Table IV shows for 1941 and 1942 the estimated failure rates which would have obtained if gas-filled cable had not been used and one half of the gas leaks found in cable sections and joints had resulted in failures during this two-year period of operation. That none of the gas leaks did cause failures may be attributed to the advantage of sheath supervision whereby sheath breaks are quickly discovered.

Electrical Characteristics

Manufacturers' laboratory test data have indicated that ionization will not take place in gas-filled cable if it is operated below an average stress of 75 volts per mil and with a minimum gas pressure of 10 pounds per square inch. All gas-filled cable operating on the Consolidated Edison Company system is supplied normally with gas at 13 pounds per square inch pressure and in case of a leak the pressure may drop to a minimum of 8 pounds per square inch for a short period of time.

The average stress for the cables ranges

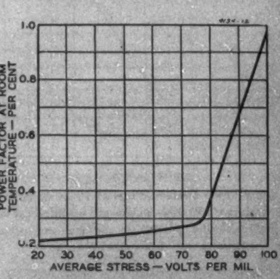


Figure 12. Ionization at room temperature of three-conductor 500,000-circular-mil 27-kv gas-filled cable after one year of operation as generating-station tie feeder

Ten pounds per square inch nitrogen pressure, 225-mil insulation

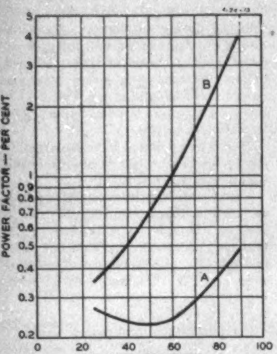


Figure 13. Power factor at 15 kv of three-conductor 800,000-circular-mil gas-filled cable before being placed in service and after approximately five years on the system

16 pounds per square inch nitrogen pressure, 150-mil insulation

A—Before placing in service

B—After 15 months loaded, 17 months dead, and 27 months loaded

between 39 volts per mil as a minimum and 68 volts per mil as a maximum. In order to determine if any ionization is taking place two sections of cable have been withdrawn for measurement of power factor and losses and for a visual examination of the papers.

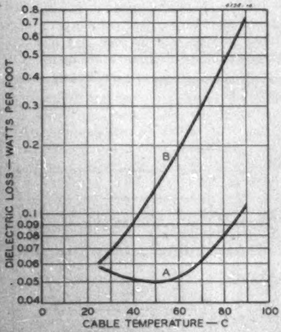


Figure 14. Dielectric loss at 15 kv of three-conductor 800,000-circular-mil gas-filled cable before being placed in service and after approximately five years on the system

16 pounds per square inch nitrogen pressure, 150-mil insulation

A—Before placing in service

B—After 15 months loaded, 17 months dead, and 27 months loaded

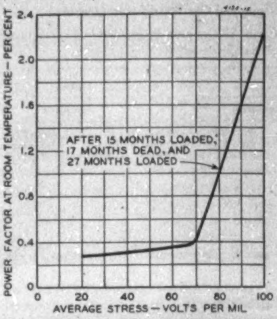


Figure 15. Ionization at room temperature of three-conductor 800,000-circular-mil 15-kv gas-filled cable after approximately five years on the system

After 15 months loaded, 17 months dead, and 27 months loaded

A 320-foot length of three-conductor 500,000-circular-mil 27,000-volt cable having 225 mils of paper insulation was removed from service after one year of operation at 27,000 volts. This cable had been operated as part of a tie feeder between generating stations.

Power-factor and dielectric-loss measurements were made on the section of cable and are shown in Figure 11 with the original factory measurements for comparison.

The power factor measured at room temperature at various volts-per-mil stress is shown in Figure 12. The ionization at 85 volts per mil after one year of operation is in agreement with data obtained from approximately 200 days of laboratory load cycle tests.

Visual examination and power-factor measurements of the paper tapes from the cable did not reveal any indication of deterioration. The five paper tapes nearest the conductor contained a few streaks, about the original color of the paper, which were more pronounced in the paper nearest the conductor. It was generally agreed that the streaks were not wax but possibly some form of oxidation products.

A 275-foot length of three-conductor 800,000-circular-mil 15,000-volt cable having 150 mils of paper insulation was removed from service after having been on the system for approximately five years. During this period the cable was operated for 15 months at an average stress of 44 volts per mil; was out of service for 17 months; and then was operated again for 27 months at an average stress of 53 volts per mil.

Power-factor and dielectric-loss measurements were made on the section of cable and are shown in Figures 13 and 14, respectively.

The power factor measured at room temperature at various volts per mil stress is shown in Figure 15.

Visual examination and power-factor measurements of the paper tapes from the cable did not reveal any indications of deterioration.

Examination of the dielectric-power-factor values shown in Figures 11 and 13 shows a greater increase in power factor for the 15,000-volt cable which had been on the system for approximately five years. With the exception of a longer period of operation the only different operating condition between the 15,000-volt cable and the 27,000-volt cable was in the amount of nitrogen which was injected into the cable after installation. Some leaks occurred on the 15,000-volt line necessitating feeding nitrogen into the line to maintain proper gas pressure thereby subjecting the insulation to exposure to a greater volume of nitrogen containing some traces of oxygen. It is possible that the insulation may absorb the oxygen from the nitrogen and an oxidation action take place resulting in an increase in power factor. This action may be the cause of the higher power-factor values for the 15,000-volt cable.

Conclusions

1. Operating experience to date has shown that electrical failures commonly experienced on new installations of solid-type cable have been eliminated by the use of gas-filled cable.

2. Breaks in cable sheaths and joint wipes and defects in terminals are ascertained immediately and remedial measures can be taken before moisture is admitted which would result in eventual electrical failure.

3. Limited data on cable in operation indicate that gas-filled cable is stable and confirms laboratory test data.

4. No serious operating difficulties due to migration of compound have been experienced with the use of gas-filled cable where there are marked differences in elevation.

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Radio-Noise Elimination in Military Aircraft

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RADIO noise may be defined as radio-frequency energy which interferes with the reception of desired signals. This interference can be the result of:

1. Atmospheric electrical discharges which result in radiated radio frequency energy.

2. Periodic corona discharges from the airplane which cause intense electric induction fields close to the receiving antennas.

3. Receiver internal noise due to thermal agitation in the receiver input resistance and shot effect in the low-level radio-frequency stages.[1]

4. Electrical transients which result from the operation of electrical equipment.

Methods of reducing the first two types of disturbances are still in the experimental stage and are not within the scope of this paper. The third type of noise is inherent in the receiver, being dependent upon receiver sensitivity, band width, and general design. This, of course, is determined by the functions which the receiver must perform.

The fourth type of noise is due to electrical transients in commutating machines, ignition systems, and other current-interrupting devices, which contain harmonic components extending into the radio-frequency spectrum. Satisfactory receiver operation dictates that this latter type of noise be suppressed or reduced to a point where the operation of electric equipment will have a minimum effect on radio communication. However, filter and shielding weight must be kept to a minimum. Therefore, radio design must be a compromise between desired radio performance and the limitations imposed by the requirements of installation, maintenance, and weight prevalent on present aircraft.

The recommendations and design procedures outlined in this paper are specifically for the frequency range of 0.16 megacycle to 20.0 megacycles. However, experience indicates that similar procedures, methods, and precautions may be applied to the ultrahigh-frequency spectrum.

In the following discussion, all wiring is considered unshielded except ignition circuits or those circuits where conduit is required for mechanical protection. The airplane is assumed to be of all-metal construction.

Radio-Noise Coupling to the Receiver

The radio-frequency energy components, coming from the various electrical transients, are induced into the receivers by various methods.

1. Radio-noise voltages generated by current-interrupting devices may be conducted into the receiver by means of the common power supply. This conducted noise is then coupled to the radio-frequency stages and results in noise in the receiver output.

2. The power wiring in the fuselage of the airplane conducts radio-frequency currents and voltages generated by the electric equipment. This results in radio-frequency induction fields which may be of large enough magnitude to induce, by magnetic or capacitive coupling, radio-frequency noise energy into the receiver-antenna leadin. Solution to this type of radio-noise problem is to reduce the mutual coupling by rerouting wiring and, if necessary, by electromagnetic shielding.

3. The ignition system of the airplane periodically (depending upon the engine speed and number of spark plugs) sets up steep-wave-front voltage discharges each time a spark plug is fired. This voltage discharge contains measurable components up to 450 megacycles.[2] The wave front of the discharge and subsequent oscillation is dependent upon the distributed circuit parameters in the high-voltage side of the ignition system.

The radio-frequency fields associated with the steep-wave-front discharges are shielded from the antennas by enclosing the magneto, distributors, and high-voltage leads with metal shielding. Efficiency of this harness shielding is dependent upon high conductivity of the entire metal enclosure. High-impedance joints between harness assemblies result in a decrease in effective shielding. Also, unless the harness is securely bonded to the airplane structure, the entire ignition harness will tend to radiate and produce resulting radio-frequency fields at the receiving antennas.

Designing for Radio-Noise Elimination

The performance of the radio equipment in the airplane is the final test of a satisfactory electrical and radio installation. Carefully tested electrical equipment which has been limited to a specified over-all radio noise voltage in the laboratory may, upon installation in the airplane, acquire a different radio-noise frequency distribution and unpredicted peaks.

It would then appear that laboratory testing is of limited value and that the prototype airplane must be tested and altered as required for satisfactory radio performance. The results of the tests could then be incorporated into the production airplanes. Although this method of design is good engineering, in actual practice there are many drawbacks to this method. Where production is of prime importance it is very difficult to effect changes after an airplane is in production. This is especially true in the case of electrical accessories. Installation of filtering on motors usually requires a redesign of the motor frame. Therefore, a compromise must be evolved whereby a maximum amount of radio design is accomplished prior to the completion of an airplane. This necessitates close co-operation between the radio engineer and the project electrical engineer.

BASIC RADIO-INSTALLATION DESIGN

The following principles of design should be incorporated on the engineering drawings at the time when the electrical and radio systems are being developed:

(*a*). Antenna lead-ins must be kept as short and direct as possible. A compromise should be effected between structure shielding and a low capacitance lead-in.[3]

(*b*). The designer should strive for a maximum spacing between the antenna lead-in and electrical wiring.

(*c*). Great care must be exercised in bonding the receiver chassis to the airplane structure. The bonding straps must be as short as possible and the bonding must be made to basic structure. Too often radio-noise coupling to the antenna input circuit is a result of poor receiver bonding.[3]

(*d*). Sources of radio noise such as commutating motors and pulsing relays should not be installed close to radio equipment.

(*e*). All radio and interphone wiring should be in bundles separate from power wiring. A minimum separation of 12 inches should be maintained if possible between communications and power wire bundles.

(*f*). Antenna location on military aircraft is usually a compromise dictated by desired radiation characteristics, the need for a short antenna lead-in, and locations available in areas which are removed from the cones of fire of the guns. Consideration should also be given to locating antennas a maximum distance from the airplane engines.

THRESHOLD OF CONDUCTED RADIO NOISE

It is desirable to reduce conducted radio noise to a level not greater than the internal noise of the receiver at maximum sensitivity.

The allowable conducted radio-noise level that will fulfill this condition must be known to permit proper filter design. This can be determined by the setup

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shown in Figure 1. Curves of power-line noise in microvolts versus receiver output in decibels can be obtained at various frequencies for each receiver. One such curve at 340 kilocycles is illustrated in Figure 2. From curves similar to Figure 2, a new curve can be derived of threshold of conducted radio noise versus receiver frequency. Threshold of conducted radio noise is defined in this paper as that value of noise voltage which increases the receiver output one half decibel above the internal noise with no signal applied. A curve of this type is shown in Figure 3. In this manner, the allowable conducted radio-noise level at the receiver can be determined in advance of the completion of the airplane.

Methods of Filtering Conducted Radio Noise

The choice of an airplane radio-noise-filter system should be governed by the following general considerations:

(*a*). The conducted radio-noise voltage level at the power input to the receiver must be within the applicable threshold levels.

(*b*). The coupling between the antenna leadin and the induction fields set up by the conducted radio noise must be kept to a minimum. Filtering exceeding the requirements of the threshold of conducted radio-noise curve specified in Figure 3, may be necessary to achieve a reduction in the radio-frequency noise induced into the antenna leadin.

(*c*). Filter weight must be kept to a minimum.

(*d*). Many changes in electrical and radio equipment are made on an airplane after it has been delivered to the customer. This problem should be recognized by the radio engineer and a safety factor allowed in his design.

There are a variety of approaches to the problem of achieving suppression of radio noise from electric equipment. General classifications are discussed as follows:

(*a*). *Receiver Filtering*. Filters may be inserted in the d-c power input to the receiver. The attenuation of the filter must be sufficient to reduce the radio-noise level below the threshold of conducted radio noise. However, this method has the following disadvantages:

1. Changes in electric equipment after the airplane has been delivered may result in a high radio noise level.

2. The requirement of low coupling of radio noise fields to antenna lead-ins is difficult to achieve without additional filtering on the power line.

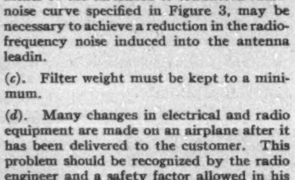

Figure 1. Laboratory setup for determining threshold of conducted radio noise for a receiver

(*b*). *Source Filtering*. Sources of radio noise have been limited by service specification to the equipment manufacturer to a value not exceeding 50 microvolts. This usually results in low radio-noise levels on the power lines. However, additional filtering may be necessary to eliminate resonant peaks. The objection to this method of filtering is the large amount of filter weight required. A source-filter installation on a large military airplane could subtract approximately 100 pounds from the pay load of the airplane.

(*c*). *Compromise Filtering*. This method is a compromise between the two methods discussed above. A limit of 500 or 1,000 microvolts can be specified for each noise source on the airplane. In the case of motors, this level can usually be achieved by the installation of a 0.1- or 0.2-microfarad capacitor across the brushes. The additional filtering is secured by the natural attenuation of the wiring and by capacitors located in appropriate junction boxes. A comparison between source and compromise filtering is shown by the curves on Figure 4. This test was conducted on a simulated-airplane-wiring network consisting of four motors, a receiver, and a power supply. Curve *A* represents the noise level at the receiver with each motor individually source filtered to a level not exceeding 50 microvolts at the motor. Curve *B* represents the over-all noise level at the receiver with each motor individually source filtered to a level not exceeding 1,000 microvolts at the motor.

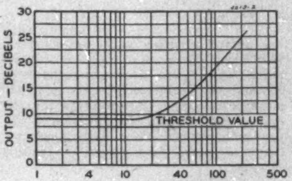

Figure 2. Typical curve of conducted radio noise on the receiver power supply versus receiver noise output for one frequency

340 kilocycles

Figure 4 indicates that very little advantage is derived from the use of heavy source filters. The 1,000 microvolt limit is not the final recommended value for this type of filtering. A value of 500 microvolts would probably be a more satisfactory compromise. In any event it would be desirable to use a level that could be attained by incorporating a small capacitor into present motor frames without necessitating a redesign of the motor. In designing for this particular type of filtering, space provisions should be allowed in junction boxes for installation of by-pass capacitors. Installations of these capacitors are determined at the time when the completed systems are tested.

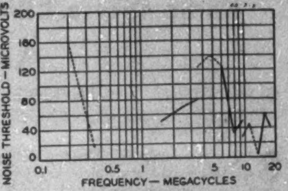

Figure 3. Curve of threshold of conducted radio noise for a receiver

The dash and solid lines indicate successive receiver bands

Ignition Radio Noise

The ignition system of the airplane sets up high-intensity radio-frequency induction fields in the vicinity of the airplane antennas. These interfering fields can be decreased by proper shielding of the ignition system. Shielding metal must be of sufficient thickness and high conductivity and each component part of the ignition shielding must be tied by means of low-impedance bonds to each adjacent member and to the airplane structure. Failure to have clean tight mating surfaces between component parts will result in ineffective shielding. On many airplane designs much engine testing is done on test stands prior to the completion of the airplane. If such is the case, an excellent opportunity avails itself to determine the suitability of the engine installation as regards radio noise.

Field-strength noise-instrument measurments can be made approximately ten feet forward of the propeller plane to indicate the severity of the ignition noise and to determine noise peaks. The ignition interference will usually resonate at approximately 9–10 megacycles. Upon determining the resonant frequency of the ignition system, the engine can then be probed to determine the efficiency of the bonding and shielding.

A Ferris model 32*A* noise instrument, or its equivalent, can be used with a probe to obtain a relative measure of the effectiveness of the bonding. A coaxial line of sufficient length and terminated with a sharp steel probe is attached to the Ferris noise instrument antenna input. The sharp point on the probe allows the test engineer to break through protective finishes on the metal in the probing operation. A ten-to-one radio-frequency voltage ratio from the magneto case to the high-voltage conduit would indicate a high-impedance bond between the two units. A high radio-frequency difference of potential between the engine and the airplane nacelle would indicate poor engine bonding.

After the incorporation of necessary

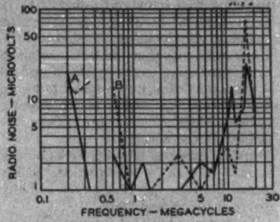

Figure 4. Comparison of source filtering and compromise filtering

A—Source-filtered installation

B—Source-, line-, and receiver-filtered installation

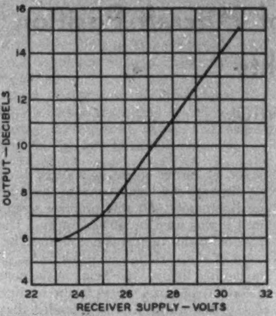

Figure 5. Variation of receiver internal noise with changes in receiver supply voltage

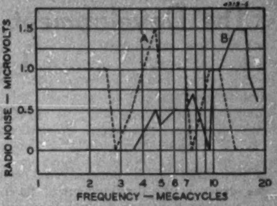

Figure 6. Comparison of the calibrated-receiver and noise-instrument method of measuring noise on an antenna

A—Noise-instrument measurement

B—Calibrated-receiver measurement

bonding and shielding changes, the field-strength measurements are repeated to determine whether additional work is necessary. Experience has not yet determined a satisfactory allowable field-strength level.

By following a procedure as outlined above, necessary changes in the bonding and shielding of the ignition system can be determined prior to the airplane's completion.

Testing the Completed Installation in the Airplane

With knowledge of the allowable conducted radio noise in microvolts for each receiver, compliance with these values can be determined by measurement at the receiver power inputs. This test should be conducted with various combinations of equipment on the airplane operating. The combinations should approximate as closely as possible service conditions. This is extremely important since various combinations of equipment may produce different levels of radio-noise voltage.

Antenna leadins must be carefully checked for radio-noise pickup from the power wiring in the airplane. An indication of the magnitude of this pickup can be determined by listening to the output of the receiver with the antenna leadin disconnected from the feed-through insulator. As a result of this test, rerouting of wiring and further reduction in radio-noise voltage by filtering may be necessary. However, in filtering, care should be exercised against increasing the magnetic-induction radio-noise field by increasing the flow of radio-noise currents. Field-strength measurements should be repeated ten feet forward of each engine and the results compared with the values obtained on the engine test stand. If necessary, further probing on the engine should be accomplished.

Over-all performance of the radio equipment, or the true measure of the effectiveness of the radio-noise design, is determined by the calibrated-receiver method. A receiver of the type to be used in the installation is calibrated in terms of a signal modulated 30 per cent at 400 cycles. Receiver output is measured with a quasi-peak-reading noise instrument of weighting circuit 10 milliseconds charge and 600 milliseconds discharge. Thus each receiver becomes an equivalent radio-frequency noise instrument. This arrangement combines the advantage of a noise instrument with the advantage of testing the actual receiver installation.

Accuracy of the calibrated-receiver method of radio-performance testing is dependent upon the following precautions and calibration technique:

(*a*). The power-supply voltage, both in the laboratory and airplane, must agree within plus or minus one half volt. Figure 5 shows how voltage variations affect the internal noise of a receiver.

(*b*). Circuit constants of the dummy antenna used in the receiver calibration should agree to within 20 per cent with the characteristics of the applicable airplane antenna.

(*c*). A minimum warm-up period of 30 minutes, should be allowed the receiver before any testing or calibration is undertaken.

(*d*). Calibration in the laboratory should be accomplished in a shielded room with a self-contained battery power supply.

By observing these precautions experience shows that results can be duplicated to within one decibel.

Readings of receiver decibel output in the airplane can be converted into equivalent microvolts from the receiver calibration curves. Maximum radio noise at the receiver input has been set by the services at 2.5 microvolts. Experience may permit a reduction in this value at a later date. Figure 6 shows comparative curves of an antenna measurement by the calibrated-receiver method and by a Ferris model 32*A* radio-noise instrument. The readings were taken as nearly simultaneously as possible. The curve of Figure 3 is for the calibrated receiver used in this test. It is interesting to note that the calibrated receiver minimum threshold values of conducted radio noise occurs in the range of 13–15 megacycles. This may explain the peak at 13 megacycles shown by the calibrated receiver in Figure 6. The Ferris instrument readings do not contain the effect of conducted radio noise.

Conclusions

1. It is possible, with knowledge of values of conducted radio noise that can be tolerated at the receiver and from tests conducted on test engines to complete a great deal of preliminary recommendations and designs which will decrease the delay in accomplishing a satisfactory receiver installation on production airplanes. The design, however, must be fluid to permit necessary changes during the tests on the prototype airplane.

2. A compromise system of filtering at source, junction box, and receiver is the most practicable and economical means of accomplishing radio-noise suppression within the fuselage of the airplane.

3. A calibrated-receiver method of evaluating the equivalent microvolts of noise should be the final test of the receiver installation.

4. Studies indicate that noise levels of 500–1,000 microvolts, if specified for electrical equipment installed in the airplane, would permit satisfactory levels of radio noise with a minimum filter weight.

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The Development of Aircraft Position Lights

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CONSIDERABLE interest has been shown in the development of flashing position lights as displayed on air-carrier aircraft at night and this paper is devoted to an explanation of changing trends in aircraft position lighting and the developmental work which finally resulted in the use of these flashing lights.

The need for a uniform system for the display and color of position lights on aircraft was soon demonstrated in the early history of aviation, particularly in Europe, where the proximity of many countries proved the need for uniformity. In October 1919 a convention met in Paris to discuss from an international point of view matters pertaining to aviation, and aircraft position lighting was one of the items which came up for consideration. Without going into any detail, it will suffice to say that it was decided that a red light be displayed on the left wing tip, a green one on the right, and a white light in the tail. The forward lights were to be visible through an angle of 110 degrees measured from dead ahead to the left and right, respectively, for the left and right lights, thus leaving an angle of 140 degrees for the tail light.

In 1926, the Civil Aeronautics Act was passed and when the Aeronautics Branch of the Department of Commerce was subsequently established, the requirements for position lights were drawn up which conformed with the principles as set forth at the Paris convention. Forward and rear lights were required to have a minimum intensity of eight candles and these requirements were in existence for years until the speed and number of aircraft operating at night had increased to the point where additional forward light was desirable, particularly for air-carrier aircraft. Accordingly, Aeronautics Bulletin number 7*E* as amended in October 1934 required that air-carrier aircraft display at least one stronger light to warn other aircraft of its presence. It was unfortunate that the requirement was so indefinite as it failed to specify the color, location, and the minimum intensity of the light. The result was that some one of the air-carrier operators inserted a 32-candlepower red lamp in one of the landing light units and this practice spread until it was used by all operators. The location of this light was not uniform because of the variation in the location of the landing lights, some being in the nose of the airplane and others in the leading edge of the wing. It was intended that this warning light be burned in conjunction with the forward lights but many of the pilots turned these lights on only when they knew that they were soon to pass an oncoming plane on the airway. The light thus was referred to as the "passing" or "courtesy" light.

The shortcomings of the warning light were soon apparent and in November 1937 the Civil Air Regulations provided for the display of a light on air-carrier aircraft having a minimum beam intensity of 35 candles directly forward. Difficulties in designing a light to meet the cutoff requirements resulted in a revision to the regulations in November 1939 which increased the minimum forward intensity to 40 candles while relaxing somewhat on the cutoff and stray-light requirements. By mid-January 1940, two manufacturers had obtained approval of air-carrier forward position lights and they were being shipped to purchasers. These lights were helpful, of course, but they did not solve the aircraft-lighting problem entirely. Congestion of aircraft over all of the larger airports where air-carrier traffic was predominant, disclosed the need for certain further improvements in aircraft position lighting. Pilots reported that they had some difficulty in locating and identifying aircraft as such which were ahead and below them. In many cases these were small slow aircraft engaged in night training flights. The pilots in overtaking ships claimed that the single white steady tail light on the aircraft ahead became one of thousands of city and airport lights which served as its background. In cases where the red or green wing tip lights were visible rather than the tail light, the lights were not easily picked up because of the number of colored neon lights in the background.

With the feeling that airplane conspicuity should be improved, at least from the rear, the Civil Aeronautics Administration was able to interest a Baltimore inventor in developing a device for flashing the tail light, the theory being that by so doing, it would be more readily discernible. The flasher was ready late in 1940 and was tested subsequently on a CAA airplane at Indianapolis by the technical development division of the CAA. The report of the tests contained many conflicting reactions but in general it was agreed that it was easier to locate the airplane by means of its flashing white tail light. Adverse criticism was mainly to the effect that many ground lights appeared as blinking lights to the airman because of obstructions which passed between the pilot's eyes and the source of the ground lights.

To gather additional information pertaining to the suitability of aircraft position lights, a questionnaire was prepared in July 1941 and sent to air-carrier operators and pilots, operating and engineering managers, and all interested parties. Replies were received from the following, as indicated:

Pilots	217
Engineering managers	5
Operating managers	11
Others	5

Some of the questions and answers were as follows:

1. Do you think the foreward lights (red and green) should be visible from a greater angle to the rear? Yes...(114) No...(106)

2. In your opinion, are the following currently used position lights adequate?

Air-carrier aircraft			
Forward lights	(40 cp)	Yes...(172)	No...(50)
Tail light	(8 cp)	Yes... (60)	No...(154)
Nonair-carrier aircraft			
Forward lights	(8 cp)	Yes...(27)	No...(168)
Tail light	(8 cp)	Yes...(46)	No...(145)

3. If considered inadequate, please describe in what respects.

As may be expected, there was a great variation in the answers but in general they indicated that greater-intensity lights were desired; that the tail light particularly should be brighter; that there should be no difference between light intensities on air-carrier and nonair-carrier aircraft; that all forward lights should have a minimum intensity of 40 candles; and that the tail light should be made more distinctive by the use of a blinker and a different-color light.

The next question is particularly interesting as it indicates to what extent pilots rely on their position lights to aid them while flying at night.

4. To pilots. (1) Do you make much use of the color arrangement on another airplane to determine its directional path or (2) are you interested only in knowing of its presence?

There were 176 who stated that they made use of the color arrangement to determine an airplane's directional path and there were 70 who were interested only in knowing of its presence. It will be noted that the total number of opinions exceeds the number of replies. This was due to the fact that some of those who replied stated that they were interested in both. They were primarily interested in knowing of the airplane's presence and then they determined

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its path by means of the arrangement of the colored lights.

5. After giving due consideration to the possible need for larger sources of electrical energy, to the resultant increase in weight, and the possible effect on the aerodynamic performance of the airplane, do you have any suggestions as to how to improve the position light or lighting system on aircraft? The suggestions for improvements were interesting and extremely varied. Many of the pilots mentioned neon, "lumiline," and fluorescent lighting. They suggested that such lighting be used in the wing tips, the trailing edges of both wings, and control surfaces. Some suggested that these lights be of the yellow sodium-vapor type, while others suggested that they be of the blinking type. In general, there was a demand for more light—either higher intensity or more lights—or both. Others wanted two additional lights in the nose of all aircraft. Several frankly stated that the airplane should be "lit up like a Christmas tree" and one felt that it "should stand out like a ferry boat on a river."

By interpretation of the results of the questionnaire, it was obvious that the pilots were generally dissatisfied with the effectiveness of the position lights. They wanted the lights to have a longer range which meant greater intensity and since so many mentioned distinctive lights and the inability to discern the aircraft against a background of city lights, it appeared that an increase in conspicuity was necessary, particularly for the tail light, as well as a more definite means of identifying the light as belonging to an airplane.

How the inability of a pilot to ascertain that a light belonged to a plane was contributary to an accident was forcefully illustrated about this time. A pilot mistook the steady white tail light of another air-carrier plane on the ground as being a boundary light some distance away and he started his take-off run towards the light. After considerable speed had been attained, the pilot became aware that he was about to crash into a plane. He immediately throttled his engines, swerved the airplane, and put his control wheel hard over, thus attempting to lift his wing over the other plane. In this maneuver he was partially successful although he did strike the outer portion of the airplane's wing.

This accident gave additional impetus to the problem of improving the safety of night flying and it was decided:

(1) That some research be undertaken immediately towards giving the tail light greater conspicuity.

(2) That some work be done towards developing a light which would make air-carrier aircraft more discernible from the front.

The need for the latter was particularly acute because of the danger of collison with Army and Navy aircraft, which for military reasons displayed little and sometimes no light. That this hazard was serious can be illustrated by quoting an operator who stated that within a relatively short time he had 178 military aircraft using the airways ordinarily flown by his pilots.

The problem of building some test units which would permit two lights to be flashed at different cycles and eclipse periods was left to the National Bureau of Standards which was ready for tests during the latter half of August 1941. Approximately 20 observers were present and they represented the Air Transport Association of America, a few air-carrier operators, manufacturers of flashing devices, the National Bureau of Standards, the CAA, and the Civil Aeronautics Board. Constant telephonic contact was maintained between the observers and the personnel in control of the lights on the roof of the East Laboratory on the Bureau of Standards grounds. With the equipment as set up, it was possible to vary the frequency from 30 to 40 to 50 cycles per minute and to change the length of the eclipse periods. The observers were approximately six-tenths of a mile away and all were equipped with pad and pencil to record their reactions to the various light flashes.

Using red and white lights which had been determined in some preliminary observations a few nights earlier as having the greatest penetrating power, the result of the evening's work indicated that alternately flashing red and white lights were best when flashed at a frequency of 40 cycles per minute with the following characteristics: 150 degrees white—10 degrees dark; 150 degrees red—50 degrees dark. The longer dark period was found to be necessary in order to make the following white flash appear as such rather than with a pinkish tint. The eye seemed to retain the red image longer than the white with the result that the dark period had to be lengthened until the point was reached when the following white flash no longer had any pinkish cast to it. It was found that even with 21-candlepower lamps in the units, the red flash lacked intensity and it was necessary to use a 32-candlepower lamp. Subsequently, in order to eliminate the need for stocking two tail lamps of different ratings, a 32-candlepower lamp was used in each unit.

With this much accomplished, the manufacturers of flashing devices had some definite requirements on which to base the design of their flashers. While they were engaged in this work, American Airlines played an important part in the development which led to the present scheme of flashing lights. During the last few months of 1941 and the first in 1942, this operator worked closely with the National Bureau of Standards and the CAA in a number of tests trying out various arrangements of steady and flashing lights. One of these tests was conducted at Washington in January 1942 and was witnessed by approximately the same representatives as participated in the earlier preliminary tests at the National Bureau of Standards. Pennsylvania-Central Airlines, which had been following this lighting project with interest by participating in the various preliminary discussions and tests, furnished a *DC*-3 airplane in which the observers rode to observe the relative conspicuity of the American Airlines airplane with and without the various flashing-light combinations. Additional tests were conducted in February while two American Airlines airplanes flew between New York, N. Y. and Hartford, Conn. During a test on February 25, 1942, a flasher was installed which gave the correct number of cycles and eclipse periods. The results convinced the majority of observers that flashing the red and green wing-tip lights simultaneously with the white tail light and the top and bottom white fuselage lights with the red tail light, gave the best results. The tests also led to the final approval by the CAA of a flasher which had been tested for the flashing schedule, satisfactory operation during three hours of vibration, high- and low-temperature effects in the range from plus 55 degrees centigrade to minus 35 degrees centigrade, radio interference, and operation with variation in input voltage. The motor-driven mechanism required about 300 watt-hours of energy per year for its operation and units are now available which can be used on 12- to 16-volt or 24- to 28-volt systems, although the first unit was designed for use on a *DC*-3 which had a 12- to 16-volt system. In appearance the flasher is a cylinder having a maximum diameter of $4\frac{1}{2}$ inches and a height of $6\frac{1}{4}$ inches. Its weight is two pounds.

With the approval of this flasher, American Airlines proceeded to install this system of six flashing lights in May 1942. The co-operation of the Air Transport Association was obtained in soliciting its members to notify their pilots to observe this system of lights in order to determine their effectiveness. The pilots apparently were impressed because in November 1942 the operations committee of the Air Transport Association voted unanimously to request the CAA that all air-carrier operators be permitted to install the system of six flashing lights. Naturally this reception was gratifying and permission was given with the proviso that it be uniformly adopted by all of the operators. It should be borne in mind that the Civil Air Regulations which had been revised in February 1942 required only that a flashing red and white tail light be displayed and, therefore, the understanding with the operators was strictly on a "gentlemen's agreement" basis. Although the date of effectuation of this requirement had to be postponed several times because of a number of difficulties, progress in completing the installations was satisfactory considering the circumstances. With the approval of another flasher in August 1942, an additional source was available which expedited the completion of the flasher installations.

By the end of 1942, practically all air-carrier aircraft were flashing three lights on each of the two flasher circuits with each carrying about $5^1/_4$ amperes. One circuit operated the standard color scheme, namely, the red and green wing-tip lights and the white tail light, while the other operated the top and bottom white fuselage lights and the red tail light. Not only was aircraft conspicuity greatly improved from the rear but from the front and sides as well and all pilots were impressed with the added sense of security. Apparently the Air Transport Command thought well of the system also because in December 1942 the CAA was informed by one of the flasher manufacturers that his units were being installed in a number of transport and cargo airplanes operated domestically by the Air Transport Command. These aircraft were flying the airways at night and the desire to maintain the high safety standard then in existence dictated that flashing lights should be installed. The value of flashing lights was accorded additional recognition nine months later when a military requirements policy issued by Headquarters, Army Air Forces, authorized the installation of a flasher on six certain types of military aircraft permanently assigned to activities within the continental United States. However, the authorization did not permit the aircraft to be altered to the extent of adding the white tail light and the top and bottom white fuselage lights. Presumably, the aircraft were badly needed and could not be withdrawn from service to enable the additional lights to be installed. At present the Navy is adopting the standard air-carrier system of six flashing lights for use on its domestically operated transport aircraft and it is our understanding that some aircraft are already so equipped.

This entire development has been aimed at maintaining the high standard of safety while flying at night on air-carrier aircraft. However, sight was not lost of the fact that nonair-carrier aircraft engaged in night flying were potentially just as dangerous to each other and to other air-carrier aircraft. From its very inception, the plan contemplated that something be done eventually to add greater conspicuity to privately owned aircraft of all sizes but it was decided that this program would be subordinate to the other. Now that the air-carrier aircraft are provided with adequate position lighting, steps have been taken to initiate something similar for other aircraft. One of the first things required is a simple, cheap, flashing device. The present devices are somewhat in the nature of precision instruments and cost about $95. A material reduction in price will be necessary before the average private flyer can afford a device of this kind. The need for a simple cheap flasher to cost about $15 was brought to the attention of several manufacturers who are now engaged in working on this problem. The unequal dark periods, namely, 10 degrees and 50 degrees, have added some complication and have been responsible, in part, for discarding certain mechanisms which otherwise might have been suitable as considerably cheaper flashing devices. With the thought in mind of simplifying the design problem, it is our intention to experiment with a flasher having both dark periods equal. The important dark period is the one after the red flash, so it is planned to have both eclipses equal to 50 degrees and to decrease the "on" periods from 150 degrees to 130 degrees. The cycle would thus be 130 degrees white—50 degrees dark; 130 degrees red—50 degrees dark. It is not believed a reduction in the "on" periods would be noticeable.

In addition to a cheap, reliable flasher, a single unit housing both the red and white tail lights is necessary before flashing lights will be commonly used on nonair-carrier aircraft. Lack of space on the rudder, the tail post, or tail cone of small airplanes will make it impossible to install two tail lights of the type currently used adjacent to each other. At least one company is known to be working on this project. Eventually it is hoped that nonair-carrier aircraft operating at night will flash as a minimum their wing-tip lights with each flash of the red and white tail lights.

The Civil Air Regulations have been revised recently to permit the flashing of the red and white tail light on nonair-carrier aircraft. Further revisions appear in order to cover similarly the flashing of the wing-tip lights and the top and bottom white fuselage lights. Before this step is taken and because the use of flashing lights appears destined to spread, a number of questions arise which are in need of further study. For instance, the point has been raised that since the red and green wing-tip lights are used to designate the forward left and right quarters, respectively, the introduction of a red light in the tail is unorthodox and may under certain conditions cause confusion. It has also been pointed out by those interested in international agreements that the use of flashing lights and the introduction of red in the tail is contrary to such agreements. Although this has been handled satisfactorily by the operators whose craft engage in foreign operation, by the use of a switch converting the wing tip and white tail lights to steady lights, the subject may warrant some further consideration inasmuch as a long-range policy for aircraft lighting should be considered.

A short questionnaire has been prepared soliciting some information from the air-carrier operators relative to the reaction of their pilots to the flashing lights now that they have been in use for about two years and it is hoped that the information received will indicate if any major changes in aircraft position lighting are necessary.

Since the presentation of this paper, the replies to the above mentioned questionnaire have been received. Fourteen of the 17 domestic air carrier operators have replied, as indicated, to the following four questions which are the most important ones of the questionnaire:

1. Are you satisfied with the present scheme of flashing the two wing tip lights and the tail light on one side of the flasher circuit and the top and bottom white fuselage lights with the red tail light on the other? Yes 14; no 0.
2. The aircraft's progress with such lights flashing has been described by a few as being "jumpy." Is this a serious objection to your pilot personnel? Yes 0; no 14.
3. Does the red light in the tail of an aircraft confuse your pilots as to its direction to an objectionable extent? Yes 2; no 12.
4. Do your pilots think the forward red and green lights should have more intensity? Yes 3; no 11.

On the basis of the replies, it is obvious that the operators as a whole are well satisfied with the present scheme of flashing lights. No changes are thus contemplated, although it is felt that the volume of air traffic in the not too far distant future will force a revision in aircraft position lighting. Such a revision will call for a pattern of flashing lights enabling a pilot to distinguish more readily the quarter from which he is approaching another aircraft.

Electric Gun Turrets for Aircraft

J. D. THOMPSON
ASSOCIATE AIEE

THIS war has seen an unprecedented development of armament, both offensive and defensive, for military aircraft. One of the most widely used and, therefore, perhaps one of the most important of these has been the power-driven gun turret, three or four of which are used on all our heavy bombers, and one or more on most smaller bombers and equivalent planes. This paper briefly surveys the field of electric-drive equipment for turrets to give a picture of the types and variety of equipment now in use and the requirements made of it, together with a description of how it works.

General Description of Turrets

Basically a turret is a power-driven flexible gun mount. In most turrets now in use the gunner is located within the turret, and by suitable controlling means drives it at any desired speed in rotation about horizontal and vertical axes so as to point the guns continually at his moving target. The gunner actually rides around with the guns and aims by peering through a sight attached to them.

In this connection it would be well to mention the two most prevalent basic types of turret arrangement. These are, first, the cylindrical turret wherein the complete turret with operator, ammunition, and so forth revolves only about a vertical axis, the guns alone being pivoted about a horizontal axis in the turret for elevation and depression. The gunner has to move his head up and down to follow the sight in elevation. The other type is the spherical turret where the gunner is at all times in a fixed position with respect to the guns, and rides up and down with them in elevation as well as around in azimuth. In both cases transparent Plexiglas enclosures through which the guns protrude are used to shield the gunner from the slipstream. Turrets of both types have been applied variously in nose and tail installations, as upper deck turrets, forward and rear, and as lower turrets projecting from the bottom of the airplane fuselage.

Much of the basic turret development was done prior to or in the early days of the war, being pioneered to a considerable extent by the British who concentrated on the caliber-0.30 automatic machine guns and used them in multiple gun installations, usually numbering four guns. American turrets from the first have used the caliber-0.50 machine gun in arrays of from one to four. By far the greatest majority, however, have had two guns, one on either side of the gunner. Larger caliber armament including 20-millimeter and 37-millimeter cannon has been used, but mostly experimentally to date.

Power-Drive Requirements

The necessity for power drives in aerial gunnery is immediately apparent from the desire for high fire power which demands more, larger, and heavier guns, and from the forces imposed on the guns by the wind velocities encountered with modern high-speed airplanes. Wind loadings on a twin caliber-0.50 turret for a modern bomber have been checked at between 300 and 400 foot-pounds of torque, varying roughly sinusoidally as the turret is rotated through 360 degrees. In addition, such a turret with full complement of ammunition, armor plate, and accessories may weigh in the neighborhood of 700 to 1,000 pounds, which must be accelerated quickly to meet the requirements of aerial combat. The top speed generally required of aircraft turrets is about 10 rpm, which represents a figure somewhat over 0.5 horsepower. Larger guns take up to 1 or 1.5 horsepower. Obviously, a human gunner could not maneuver the guns to any reasonable degree without the aid of a power drive.

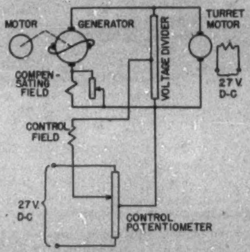

Figure 1. Basic Amplidyne control circuit

The requirements for a turret drive are quite stringent. The guns must be under perfect control at all times if accurate shooting is to be accomplished, and no looseness in the response to the controls is to be tolerated. Perfectly smooth steady operation must be obtained over the entire speed range from zero to the top speed of the turret, and the turret speed should be as insensitive as possible to the changes in wind loading when it revolves. High accelerations and quick reversals must be obtainable, and the equipment must be light, simple, trouble-free, and easy to maintain. In addition, it must perform equally as well at altitudes of 40,000 feet and ambient temperatures of −65 degrees Fahrenheit as it does under normal conditions or under conditions of desert heat. Of course it must withstand the shocks and vibrations incident to use on military aircraft.

Types of Drive Equipment

Both hydraulic and electric-power-drive systems have been used successfully for turrets. The hydraulic drive was long favored because of the smoothness and slow speeds which were obtainable, and in more powerful designs because of its light weight. However, control difficulties, problems of leakage, cold weather troubles, and vulnerability have militated against it to an appreciable extent. At the present time, in this country at least, hydraulic turrets are in the minority.

The wide range of load and speed required makes an unusual control problem for aircraft electrical equipment. To date the most successful solution to it has been use of the Amplidyne type of generator, and by far the greatest majority of electric turrets are now Amplidyne controlled. The Amplidyne is especially suited for turret control because of its quick response and high power amplification. These features permit accurate control and the use of a very small amount of control power, which means the utmost in small and simple control devices.

Amplidyne Control Circuit

Figure 1 illustrates the circuit in general use with Amplidyne turret drives for controlling the speed of the turret-drive motor. It is fundamentally a variable-voltage system with negative-voltage feedback for regulation and stability. The controlling means is a center-tapped

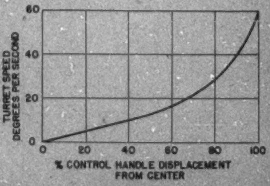

Figure 2. Typical speed-response curve for a turret

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potentiometer fed from the airplane's 27-volt d-c supply, and exciting the low-wattage control field of the Amplidyne. Turret motor speed is controlled by varying the magnitude and polarity of the Amplidyne field excitation. The turret motor is a separately excited shunt machine, so it is necessary to compensate the Amplidyne (by means of a series field) somewhat over 100 per cent to overcome *IR* drop and inherent speed regulation of the motor, and thereby maintain good turret speed regulation under high loads. Such a system would be unstable by itself at lower speeds and loads, and so a portion of the Amplidyne output voltage is fed back into the control field circuit for stabilization. This is obtained from a resistor which constitutes a voltage divider across the Amplidyne output. The field excitation is the difference between the potentiometer output and the voltage from this divider. The position of the divider tap determines the Amplidyne output voltage for any control potentiometer setting, making this in effect a speed regulator circuit. With a linear control potentiometer the turret speed characteristic, with respect to control handle displacement, is a straight line. This is quite positive and gives both high acceleration of the turret in response to the control handles and a dynamic braking effect for holding the turret steady against the wind, even when the control potentiometers are not energized.

With the machines commonly in use this circuit has a time constant of response of about 0.04 second and can accelerate a turret up to 500 degrees per second per second. Because of the high inertias involved at such accelerations, it is necessary to antihunt the system. To accomplish this an additional field is connected across the Amplidyne output through a capacitor and is of such polarity as to buck down any voltage produced by the control field. This arrangement merely prevents an oversensitive Amplidyne from accelerating the turret too fast.

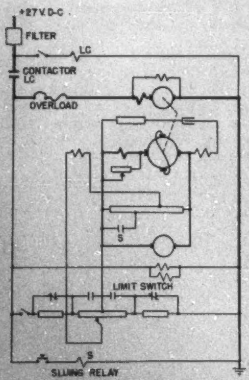

Figure 3. Typical turret-drive elementary diagram

Sluing Circuits for Amplidyne Control

The basic circuit described previously is readily adaptable to modification by several types of additional control and safeguard circuits useful in turrets. One of the most important of these is a sluing circuit, which amounts to a gear shift for tracking speeds. When tracking a target the gunner seldom needs to operate the turret at a high rate of speed, and it is not desirable to have too great a change in rate for a given motion of the control handles, as this makes control more difficult. For tracking, a voltage divider tap is used which gives a maximum turret speed of 20 or 30 degrees per second. But the gunner also wants to be able to shift rapidly from one target to another, that is, slue the turret. This can be accomplished by changing the voltage divider tap so as to obtain full Amplidyne output. If desired, the tap can be changed gradually by means of another potentiometer geared to the control handles so as to change the slope of the response curve gradually, as shown in Figure 2.

Figure 4. Amplidyne for turret drive and speed control

Limit Circuits for Amplidyne Control

In addition, a limit circuit is often desirable to prevent the gunner from running the guns down full speed into the side of the airplane. Such a circuit must have an automatic backout feature, and it must be simple. It is accomplished easily by having a limit switch short circuit that side of the control potentiometer which drives the guns toward the limit.

Miscellaneous Circuit Modifications

Generally an "action switch" or "deadman" is incorporated in the control handle to cut off power from the control potentiometers when the handle is released. This is a safety feature to prevent the turret from swinging around in the event that the controls should be knocked accidentally when power is connected to the turret.

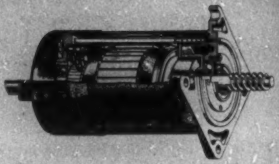

Figure 5. Sectional view of typical turret motor showing brake coil and mechanism near pulley end

It often is considered desirable to have brakes on the turret to prevent its swinging around in the wind when power is off. A nonreversible worm drive would accomplish the same thing but is rather inefficient. For this purpose some turret motors have brakes built into them, which release magnetically when power is applied to the field.

Figure 3 shows a typical turret control circuit elementary for the elevation motion. The Amplidyne is driven by a compound-wound motor directly off the ship's 27-volt d-c supply. It is started by a relay, has thermal overload protection, and there is generally a radio-noise filter in the line. The turret motor field and brake are energized at the same time the Amplidyne is started, so as to prevent circulating currents flowing around the loop circuit between generator and motor armatures. The control potentiometer is energized by a separate "action switch," and sluing is obtained by means of a relay. Upper and lower limit switches with current-limiting resistors are included. Miscellaneous accessories are not shown.

Turret Amplidynes

The Amplidynes now in general use are two-bearing motor generator sets 12 inches long and 29 pounds in weight (see Figure 4). The motor is compound-wound with eight poles, rated at 27 volts and 44 amperes direct current. It runs in ball bearings at 8,300 rpm. The generator is rated at 60 volts, 8.8 amperes (530 watts) for ten minutes, and at this output requires about 60 milliamperes in the 25-ohm control field (0.1 watt). These machines also have a $1^1/_2$ minute rating of 750 watts, corresponding to the maximum wind load torques on the turret, and will put out over 1,000 watts for short times, necessary for breaking away a frozen or iced-up turret. Because it is expected that they will not have long life in such an application, the temperature rise generally is considered to be limited by what the commutators will stand before throwing solder. All connections are made

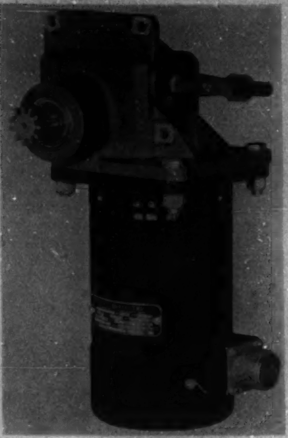

Figure 6. Turret motor and gearbox showing pinion to mesh with turret-ring gear and lever to declutch motor

Gear reduction: 10 to 1

through a single *AN* connector, and two rheostats are mounted on the side for shunt adjustment of the compensating field. A fan is located on the motor end of the machine, and all rotating parts are balanced dynamically. Vibration is quite important insofar as it affects generator commutation, which is extremely critical for smooth turret operation and correct compensation. Special brushes are used for satisfactory life at high altitudes, consistent with reasonable commutator and brush life at sea level.

Turret Drive Motors

The drive motors for the turrets are rated at 60 volts armature and 27 volts field, and generally are rated at 4,000 rpm. Good performance is required, however, over the entire speed range and particularly at very low speeds. Sticky bearings or any tendency for slot-locking will give ragged turret performance at the lowest speeds where accurate aim is usually most important. The motors must be able to take whatever the Amplidynes can put out, and this often may mean almost instantaneous reversal of maximum applied armature voltage. Of course they must run equally well in both directions, and the maximum speeds in opposite directions should be essentially equal. When space permits they are supplied with a fan for cooling, but its value is questionable, as the motors must run at low speeds and full torque for much of the time. A typical turret motor is shown in Figure 5. It is seven inches long and weighs about ten pounds. Its ten-minute rating is 0.5 horsepower at rated voltage and 8.3 amperes. Figure 6 shows a similar motor mounted to a gear reduction box for driving a turret. Note that a lever is provided to declutch the worm-drive gearing to make the turret easier to rotate manually in the event of power failure. The over-all efficiency of a drive system using such motors and Amplidynes is in the neighborhood of 40 per cent (not including turret gear efficiencies).

Control Units for Turrets

Satisfactory control equipment for easy, efficient, and accurate manipulation of the guns requires a carefully designed control unit. The controls above all must be simple and "natural;" that is, their arrangement must be such that their correct operation is virtually instinctive, with as little as possible for the operator to learn. They must be operable with the minimum expenditure of thought and energy on the part of the gunner, and they must be comfortable and easy to use. When it is considered that the gunner may have to operate the turret for many hours on end, often under conditions of extreme temperature and altitude, it is understandable that much consideration must be given to the ease and comfort of his work if his efficiency is to be maintained.

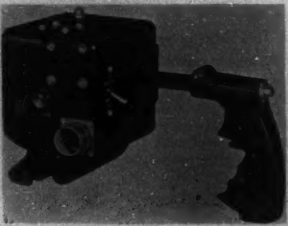

Figure 7. Joy-stick-type control unit for spherical turret

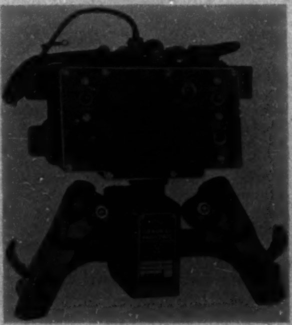

Figure 8. Typical turret control unit with standardized grip arrangement

Many types of control handle arrangements have been tried. Steering wheels, single or double joy-sticks, rotating grips as on a motorcycle, and various types of handle-bar arrangements all have been used. There is no doubt that operation of most any arrangement can be learned, and also that different turret arrangements may require different control arrangements. Figures 7 and 8 illustrate two typical control units, one a joy-stick type with pistol grip, and the other a double-grip arrangement. The former has been quite satisfactory in a spherical-type turret. For cylindrical turrets the double-grip arrangement of Figure 8 has been found most satisfactory, and it has been standardized wherever possible. In this arrangement both grips move together and rotate on a horizontal shaft for elevation control and about a vertical axis midway between them for control of azimuth rotation. The two grips customarily are placed close together so as to be operated primarily by wrist motion, so as to minimize fatigue. Likewise, friction and inertia should be low, and any spring centering kept very light. However, there still must be enough friction and inertia to give the operator something substantial to hold onto, when the airplane bounces or maneuvers, without upsetting his degree of control. The inertia of the operator's arms and body is important here in creating a control system which is not subject to jerking during acceleration.

In this typical control unit the grips are of plastic material. Each grip has a lever on the side which, when depressed by the weight of the gunner's hand, energizes the control circuit. There is also a gun-firing trigger switch on each grip, operated by the forefinger, and a push button accessible to the thumb for sluing, or for an interphone if desired. The grips can rotate 22½ degrees either side of the center position for both elevation and azimuth control and are geared directly to the control potentiometers. The latter are of sufficient diameter and close-wound with fine enough wire to give smooth control without noticeable steps in the change of speed. The upper portion of the unit is devoted to a control panel with switches, rheostats, and overload resets to control the sight, firing circuits, and miscellaneous accessory equipment. Connections are made through standard *AN* connectors and fittings.

Electrical Accessory Equipment

The electrical gun-firing and accessory equipment being used on turrets nowa-

Influence of Electricity on Aircraft Instrumentation

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MEMBER AIEE

Synopsis: Many factors have operated to set up conditions which have been very favorable to the electrification of aircraft instrumentation. Two tabulations are presented to indicate the extent to which electrical principles have been applied to common aircraft instruments. The effects of increased size in aircraft, larger power plants, longer flight time, association with other equipment, and other factors upon instruments are discussed. The advantages of electric instruments are presented, and some possible future developments are noted.

CURRENT bibliographies on aircraft instruments list a reference to an early article dated 1904—40 years ago. This paper, describing an altimeter, has been followed by thousands more indicating the interest and technical advancements in aircraft instruments. The needs of the aircraft industry have been met by the instrument manufacturers. Developments in instruments have paralleled the rapid developments in aircraft due to the close co-operation of measurement and aeronautical engineers. A typical aircraft instrument panel is shown in Figure 1.

Since few manufacturers make a complete line of aircraft instruments, an overall listing of the devices included in this field is difficult to find. Textbooks, bulletins, and trade catalogues list instruments under various classifications. The devices covered and the typing are seemingly influenced by popular terminology and the author's particular interest. Another classification is not, then, out of order.

Aircraft instruments can be identified by the function which they perform. A tabulation made on this basis should be of interest in cross-checking the needs of the aircraft with the instruments available. Aircraft instruments can be grouped by the type of instrument and by the basic principle of operation.

Because many factors have operated to set up conditions which have been very favorable to the electrification of aircraft instrumentation, particular emphasis can be placed on this effect by segregating the electric instruments from other types.

The increase in use of electric instruments is traceable to the following factors:

1. Increase in size of aircraft.
2. Increase in size of power plants.
3. Longer flight time.
4. Desirability to fly under adverse conditions.
5. Nonelectrical methods not feasible.
6. Association with other equipment.
7. Adaptability to available power source.

Since electric devices, other than instruments, are being used in increasing numbers on aircraft, it is anticipated that electric instrumentation will progress.

Scope

An instrument is normally considered a measuring device. In the control of any process, regardless of its nature, a measuring device may function in two ways:[1]

1. It may give visual indication of events or conditions which may govern the manual adjustment of apparatus controlling these events or conditions.
2. It may initiate actions which automatically adjust equipment controlling events or conditions.

Only instruments in the first group are considered, since control devices such as voltage regulators, fuses, and speed-control apparatus are included in the second group. The aircraft industry operates in general within this definition. Two exceptions are warning lights, which are not considered to be instruments, and automatic pilots, which are classed as instruments. Interlocking operation, so easily accomplished by electrical means, makes the distinction between an instrument and a control device increasingly difficult.

Classification

Table I shows aircraft instruments by basic type of measurement. This table may not be comprehensive, that is, it may not include all instruments, but it does show all instruments used in any quantity. Automatic pilots are not tabulated, since their function other than control is duplicated by other gyro instruments.

Table II shows the aircraft instruments segregated into their classes: flight, navigational, engine, and miscellaneous with the basic principle of operation listed. Further classification is made by dividing the instruments into groups indicating that the instruments operate as self-contained units or consist of two units, a transmitter and a receiver, to give remote indication. The means by which the basic principle of operation is effected is also shown.

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days has reached considerable proportions. Guns are fired almost without exception by electric solenoid devices mounted on the guns, and drawing from four to eight amperes. Usually both guns are fired simultaneously, although sometimes provision is made for separate firing of the guns when desired. Cam-operated fire interrupter switches are used in these circuits to cut out the gun fire in interference regions, so that gunners will not inadvertently shoot away the tail or wings of their own airplane.

Because of the large amounts of ammunition carried, the feeds to the guns are long and tortuous, so that ammunition boosters are required. These are ratcheted sprockets powered by small intermittently rated electric motors which operate when tension in the ammunition belt reaches a predetermined value. They generally take considerable current; 20 amperes is not unusual.

Automatic electrically driven movie cameras aimed along the line of fire are often used to record the effect of the gunfire. They are energized whenever the firing trigger switches are closed.

Additional electrical accessory equipment often used includes the interphone, a rheostat-controlled power outlet for electrically heated clothing, defrosting heaters and fan for the Plexiglas dome, and usually a "trouble light" on the end of a flexible cord for general illumination.

The total power drain of a 27-volt turret with everything running at once, and going at full speed against the wind may be well in the neighborhood of 175 amperes.

Conclusion

Amplidyne turret drive and control equipment is now in use on thousands of turrets of several different types and manufacture all over the world. It lends itself readily to many control-circuit variations and mechanical arrangements and is eminently simple and easy to service and maintain. All reports have indicated that it is thoroughly reliable and performs its proper functions satisfactorily.

Figure 1. Typical aircraft instrument panel

There are some measurements, such as exhaust-gas analysis, which are made only by electrical means. Some measurements, such as time, are made only by mechanical devices.

It is indicated that many measurements can be made by devices utilizing both mechanical and electrical principles in their operation. While certain instruments, fuel-quantity gauges, for example, can be obtained operating on more than one principle, general usage is in favor of those utilizing electrical means. Figure 2 shows the relative quantity of instruments available in the various classifications.

Factors Favoring Electric Instrumentation

Airplane Size

The size of aircraft has increased tremendously. Wing spreads over 200 feet are not uncommon. Engines are placed out on the wings with the obvious result that engine instruments are principally electrical in operation or utilize electric transmission. Electric tachometers eliminate the impossible task of using flexible cable drive through a distance of as much as 60 feet between the engine and the instrument panel. The saving in weight by eliminating the use of cable is many pounds. Synchronization of multiple engines is indicated easily by electric synchroscopes operating from tachometer generators.

Many pressure indicating devices utilize electrical transmission to eliminate long heavy pipe lines, thus reducing installation costs, maintenance difficulties, and operational hazards by avoiding the danger of leaks and by decreasing vulnerability to gunfire.

Other parts of the airplane are often so far from instrument or control panels that the electrical method is the only practical means of measurement. Gasoline tanks are multiple in number and placed in all parts of the airplane. Landing gear, flaps, and so forth cannot be seen by the pilot, and position indication must be provided.

Remote-indicating electrical compasses[2,3] are a necessity because of the impossibility of maintaining a uniform magnetic field in the cockpit. The pickup element must be located in a remote position, such as the tail or wing, to secure accurate readings.

Much has been written on the subject of power systems in aircraft. Increased power loads and the use of 400-cycle supply dictates the use of ammeters, voltmeters, wattmeters, and so forth, to assure proper control.

Table I. Classification of Aircraft Instruments

		Specific Measurements	Instruments
1. Measurement of Quantity			
Absolute	Solids, liquids, gases	Fuel quantity	Flowmeter
		Airplane height	Radio altimeter
		Ice accumulation	Rate-of-ice-accumulation indicator
	Electricity	Electric load	Ammeter
With respect to time. . Power.	Mechanical	Engine horsepower	Horsepower indicator
	Electrical	Generator load	Wattmeter
2. Measurement of Quality			
Composition analysis		Fuel mixture	Exhaust-gas analyzer
3. Measurement of Condition			
Temperature		Outside air Oil Coolant Carburetor mixture Inside air	Thermometer
		Engine cylinder Jet exhaust	Thermocouple
Pressure		Oil Fuel Manifold Suction Deicing equipment	Pressure gauge
Speed		Engine rpm	Tachometer
		Engine synchronism	Synchroscope
		Airplane rotation	Rate of turn indicator
		Airplane acceleration	Accelerometer
		Electrical frequency	Frequency meter
		Airplane forward speed	Air-speed indicator
Potential		Electric potential	Voltmeter
Displacement	In space	Oil quantity Fuel quantity	Liquid-level gauge
		Wheel position Flap position Oil-cooler flap position Cowl flap position	Position indicator
		Level flight	Gyro-flight indicator
		Direction	Directional gyro, compass
		Weight	Altimeter
		Change in height	Rate-of-climb indicator
		Pitch angle	Inclinometer
		Stall conditions	Stall indicator
	In time	Drift	Driftmeter
		Time	Clock

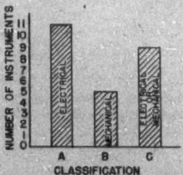

Figure 2. Summary showing relative quantity of instruments in various classifications

POWER PLANTS

As the size of aircraft engines has increased, the necessity for improved control has been noted. Carburetor air temperature requires a sensitive measuring device capable of following quickly any change in temperature. The resistance bulb and ratio-type temperature indicator fills this need. Improved cylinder-head temperature indicators are being developed to permit engines to be operated at higher efficiency. The position of cowl flaps and oil-cooler flaps is indicated by electric instruments.

The very high temperatures encountered in jet-propulsion engines are being measured by thermocouples which constitute the only practical means.

Longer flights dictate rigid maintenance to flight schedules and the attainment of high efficiency in operation. Fuel flowmeters, utilizing electrical transmission, are standard equipment. New and improved horsepower indicators[4] aid in obtaining efficient engine performance.

FLIGHT CONDITIONS

Airplane performance has been improved, so that flights are made under extreme conditions of weather, altitude, and temperature. Electric instruments aid by providing not only good performance in general under these conditions, but also provide some specialized indications. Rate-of-ice-accumulation instruments are electronically operated. Pressurized cabins for high-altitude flight present difficult problems in that the tubing and other connections for mechanically operated instruments must be sealed at the point of cabin entry. Airtight electric connections are made easily by gasketed lead-in studs.

LIMITED METHODS

Certain measurements are made by one type of instrument because no other method appears practical. Clocks will

Table II. Operating Principles of Aircraft Instruments

	Self-Contained				Remote Indicating				
Class—Instrument	Electrical	Mechanical	Mechanical–Electrical	Mechanical–Pneumatic	Electrical	Mechanical	Mechanical–Electrical	Mechanical–Hydraulic	Mechanical–Pneumatic
Flight Instruments									
Altimeter		Aneroid							
Rate of climb		Manometer							
Air speed									Manometer
Free-air temperature					Resistance measurement			Pressure gauge	
Accelerometer		Mass measurement							
Inclinometer		Pendulum							
Flight indicator			Gyroscope	Gyroscope					
Rate of turn indicator			Gyroscope	Gyroscope					
Stall indicator			Pressure switch						
Navigational Instruments									
Directional gyroscope			Gyroscope	Gyroscope					
Compass		Compass			Magnetic saturation		Compass-magnetic saturation		
Clock		Escapement							
Driftmeter			Telescope						
Engine Instruments									
Pressure indicators							Pressure gauge—electric transmission	Pressure gauge—hydraulic transmission	
Temperature indicators		Bimetal			Resistance measurement and thermocouples			Pressure gauge	
Tachometer					Magnetic-drag electric transmission	Magnetic-drag machine coupling			
Synchroscope					Frequency comparison				
Fuel and oil-quantity gauge		Sight-gauge and float type					Float level—electric transmission	Float level—hydraulic transmission	
Flowmeter		Volumetric pump					Volumetric pump Pressure measurement Velocity measurement } Electric transmission		
Horsepower indicator							Combined torque and speed indicator		
Exhaust—gas analyzer					Thermal conductivity—resistance measurement				
Miscellaneous Instruments									
Position indicators					Resistance measurement				
Ammeter	Direct current—permanent-magnet moving coil								
Voltmeters	Alternating current—moving-iron vane								
Wattmeters	Electrodynamic								
Frequency meters	Series resonance								
Ice accumulation					Capacitance measurement				

FOLLOW UP CONTROL
HYDRAULIC SERVO
SERVOAMPLIFIER
DIRECTIONAL

Figure 3. Components of an electric–hydraulic automatic pilot

continue to be mechanical until better regulated electric power sources are available. Exhaust-gas analyzers and distant reading compasses have been mentioned previously. Radio altimeters are the only instruments available for obtaining absolute height above ground. It is expected that instruments operating entirely on electrical principles will assist in obtaining better fuel quantity measurements.

Associated Equipment

Electric instrumentation is easily adapted to meet requirements calling for the combination of two or more measurements or of measurement and control functions. The horsepower indicator combines engine torque and speed. The electric tachometer and synchroscope work as a team. Electric compasses are used to control the precession of directional gyroscopes. Moreover, the remote totalization of several measurements can be effected easily to determine, for example, the total quantity of fuel in a number of separate tanks.[5]

Electric gyroscopes, as used in automatic pilots, are outstanding examples of combined instrumentation and control. Figure 3 illustrates the components of an electric–hydraulic automatic pilot. The electrically operated gyro instruments[6] incorporate electric pickoffs which give the required signal for automatic pilot control. The operation of the flight indicator and the directional gyroscope as indicating instruments is not impaired, since the signal power required is negligible. Electronic amplifiers provide means for obtaining any power step-up required. Figure 4 identifies the functions which can be controlled by electric pilot instruments.

Other examples of associated instrumentation and control are overspeed control of turbosuperchargers which utilizes the tachometer generator, combining positioning motors and position transmitters, and automatic propeller synchronization.

Adaptability to Power Source

Many instruments are available in forms to operate from d-c or a-c power sources. In addition, inverters, converters, and rectifiers can be used so that electrical instruments are applicable regardless of the electric system of the airplane.[7] Figure 5 illustrates circuits commonly used for remote transmission of aircraft measurements.

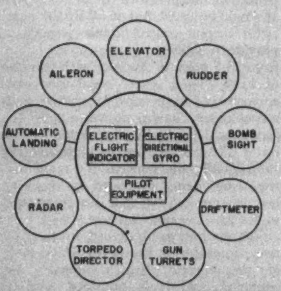

Figure 4. Possible airplane functions controlled by electric automatic pilots

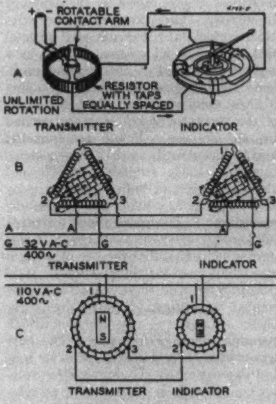

Figure 5. Typical circuit

A. D-c remote indication

B. A-c remote indication

C. A-c remote indication—induced second-harmonic system

Conclusion

A review of the common aircraft instruments indicates that many operate only on electrical principles and that electric transmission for remote indication has been applied extensively to others. A few instruments cannot be improved by the use of electricity because of their inherent principles of operation or method of application, and these, no doubt, will continue to be mechanical.

Many factors have influenced the electrification of aircraft instruments. Because of the work done, the instrument needs of the aircraft manufacturer have been met, and many new instruments having superior characteristics have been developed. It is anticipated that technical improvement in aircraft instrumentation will continue, and that the benefits obtained from the use of electricity in this field will increase.

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Brief Survey of Power-Supply Developments on British Aircraft

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THIS paper covers the development in broad outline of electric generating equipment and systems on British aircraft from the 1920's up to the present time. The coverage, of necessity, must be very sketchy, but it is hoped that outlining British development in its approximate chronological order may be helpful in comparison with American development over the same period. No attempt will be made to go into great detail, and so pictures and curves have been omitted.

D-C Power Supplies

Early airplanes, which were mostly of the biplane type, used exclusively the so-called 12-volt d-c system. Power was supplied by a "windmill"-driven generator of 14 volts 500 watts capacity. "Windmill" is the British word for the wooden or metal propeller which was actuated by the slip stream of the aircraft, thus rotating the generator armature at its rated speed of approximately 5,000 rpm at the normal cruising speed of the airplane. Various "windmills" of differing pitch were available to take care of the various aircraft which had naturally differing cruising speeds. The generator was built in streamlined form and was mounted usually on the wing. Connection was made at the rear end by a plug and socket, and the weight of the complete assembly was about 20 pounds. Cooling was, of course, by the slip stream and care had to be exercised not to load up the generator to its maximum capacity when running up on the ground for test purposes; 40 per cent full load was the specified safe limit under these conditions unless an air blast was available.

Voltage control of these generators was achieved by a vibrating reed or Tirrill regulator, and also in the same box was located the reverse-current relay. It is worthy of note that these early regulators were screened in a metal box to minimize radio interference and the connecting cable was screened for the same purpose. Round about 1935, research and development were accelerated for obvious reasons which need not be discussed here, and about that time the airplanes which were destined to be our mainstays during the early part of the war were in various stages of development or production. These were the Spitfire and Hurricane fighters and the Whitley, Wellington, Hampden, and Blenheim bombers. All of these were fitted with a new type of engine-driven generator with self-regulating characteristics, which had been developed. The adoption of this generator, by rendering an external voltage regulator unnecessary, solved troubles which had been experienced with Tirrill regulators, the most bothersome of which had been radio interference.

It may be of interest to describe this generator in some detail. Three field windings were provided, a shunt winding for the primary excitation, a series winding to compensate for voltage drop in the generator due to load and thirdly, a control winding. This latter winding was arranged to carry battery-charge current only, and the resultant field was in such a direction as to check rising voltage with increasing engine speed, by the tendency to increase charge current. The principle worked out reasonably well in practice, but, like all excursions into new regions, it created its own peculiar problems which will be noted later. Cooling of these units was by air blast, the whole of the generator yoke being surrounded by an airspace. Power requirements also had increased, so that these generators were made available in 500 and 1,000 watt sizes at both 14 and 28 volts and weighing 22 pounds and 36 pounds, respectively. The speed range for rated full load was 3,000 to 5,500 rpm. It was during this period that the 24-volt system was tending to oust the 12-volt.

Experience with these generators under combat conditions during the early phases of the war revealed the limitations governing their performance. The battery-charge current varied between zero and approximately ten amperes for voltage control and, in general, bore no relation to the state of charge of the battery itself. Thus, if a battery were discharged by ground tests and repeated engine starting, the battery could not accept a heavy recharging current during flight, and undercharged batteries, useless in a crisis, resulted. Further a fixed resistance, switched into the field circuit by the engineer when the voltage reached 14.5 volts, had been found necessary, but this needed vigilance and was therefore undesirable. Another undesirable feature was that a resistance had to be substituted for the battery when the latter was removed from the airplane; otherwise the generator was uncontrolled if the engine was run up. Lastly the voltage characteristics were not stable enough to allow paralleling, and this last problem rapidly was becoming important.

The power-supply policy therefore swung back to shunt-wound generators with external voltage control, and the movement was accelerated by the development of carbon-pile voltage regulators. The generators were built primarily for 24-volt systems but 14-volt units were made available in the 500-watt size for incorporation in later Spitfire and Hurricane aircraft. The speed range remained the same, and it is worth noting here that differing speed range, mounting pads, and drive shafts are the chief stumbling blocks to standardization between American and British units. A new unit of 1,500-watt capacity was added to the range, this being entirely due to improvements to the existing 1,000-watt model.

A brief history of British development of the carbon-pile voltage regulator for aircraft use may be of interest here. The principle is, of course, not new. The first model to go into production differed widely from the type now in general use, and in some aspects this early regulator possessed definite advantages over the now standard model. The pile was exposed and not enclosed in a ceramic tube, and the compression on the carbon stack was effected through a link system giving longer travels which were more easily controllable. The magnet system consisted of a specially shaped armature rotating on ball bearings between the magnet pole shoes, and a dashpot with a graphite piston was provided for damping to prevent hunting. Temperature compensation was provided by an Invar rod. It should be noted that on all British regulators current control is incorporated to provide a voltage characteristic falling from 28.5 volts to 26 volts with increase of load. These early regulators were comparatively expensive to produce, and, a simpler type having shown promise, the former was gradually superseded by the latter. This is the Newton type, and it is substantially the same as the regulators which are being produced in quantity here in America, and so it is not proposed to describe this in detail here.

The equipment described in the last two paragraphs is that with which most of the current British aircraft are equipped, and it is proposed now to sketch briefly present developments in the d-c field; the position regarding a-c development will be treated separately and discussed later. Two generators will soon be in production, one of 3,000 watts capacity and the second of 6,000 watts. By a curious coincidence they

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are types *O* and *P*, and thus they correspond in output to American units of similar nomenclature. The weights are 50 pounds and 55 pounds respectively, but it is believed that an improved 3,000-watt model is under development weighing 35 pounds. These figures compare unfavorably with the American designs, but it must be remembered that the speed range remains 3,000 to 5,500 rpm, and also that the rated output is given continuously.

Voltage regulators have been improved in detail, and the most important change has been to provide two carbon stacks in series in the one regulator, one for voltage control and the other to act as a current limiter instead of the voltage coil and current coil operating on one stack. On single-generator installations the voltage control keeps the voltage constant up to the rated output of the machine, and then the current limiter comes into operation to drop the voltage sharply. In multiengine aircraft where generators are paralleled, each regulator gives a falling voltage characteristic with load until full load on each generator is reached, after which the current limiters act as before. In addition the load current is taken through a small coil acting on the voltage pile to reduce the voltage of the generator if it tends to take more than its share of the combined load.

Experiment and research are going forward with the paralleling of four of these larger 6,000-watt units in order to get stable installations. The scheme showing most promise incorporates a fifth master regulator which is actuated by the bus-bar voltage to keep this substantially constant, irrespective of the voltage tendencies of the individual generators feeding the system. As already explained, each generator is controlled by its regulator to give a falling voltage characteristic with load, and also compensation is provided for equal load division. In addition a tapping is taken from the swamping resistance in series with the voltage coil of each regulator, a few volts up from the negative bus-bar end. These four tappings are strapped together, and the carbon stack of the master regulator is placed between this common point and negative, that is, in parallel with the sections of the swamping resistances. The pile is actuated by a coil across the positive and negative bus bars, and is arranged to open with falling and close with rising voltage. The combination thus gives substantially constant bus-bar voltage, even though the voltages of the generators individually are tending to fall with load.

Radio and Radar Power Supplies

At about the same time that d-c supplies were being obtained from a "windmill"-driven generator, radio power was also derived from a similarly actuated source. This streamlined machine was built to give 0.1 ampere at 1,200 volts and four amperes at ten volts, weighed 20 pounds, and was designed to run at a constant speed of 3,500 rpm. To give this last characteristic a slipping clutch, or a variable pitch "windmill," was used.

Following this, a range of motor generators was built which ran off the aircraft electric system. The outputs were in all cases 80 watts either at 1,200 volts or 600 volts, and the inputs were 12 volts or 24 volts to suit the aircraft main power supplies. These units weighed about 12 pounds and were approximately 4.5 inches in diameter by 11 inches long. An interesting point here is that a limit of 80 watts was placed on radio requirements from the aircraft battery, until the summer of 1939, when the demand suddenly was jumped to 750 watts.

Then came the war, and the urgent need for more and more power for radio and radar arose, and thus was born the engine-driven alternator, which to this day is the method most favored by the British for obtaining this power. The first model to go into production was a single-phase alternator rated 500 watts at 80 volts rms, and when fitted to British engines its speed range was 3,000 to 5,500 rpm. The frequency was 1,300 cycles per second at 3,000 rpm. Incidentally, it is not known by the writer what reasons governed the adoption by the British of 80 volts and the frequency range 1,200 to 2,400 cycles per second as the standards for these supplies.

The design of these alternators permits of a very robust mechanical unit. The field is energized by direct current from the aircraft's main d-c power supply, and no windings are placed on the rotating mass, the field coils being positioned on a conical-shaped projection of the end plate, which is bored to receive a cylindrical part of the rotor. The stator with the a-c windings is fixed to the yoke, and there are 24 teeth on the inside periphery. Aligned with the stator teeth are the rotor teeth, 26 in number, so that a pair of teeth coincide 26 times per revolution. The flux path is thus from the conical projection across the constant air gap to the cylindrical part of the rotor along the rotor to the teeth and across the air gap to the stator teeth. The magnetic circuit then is completed through the yoke. The flux is unidirectional but varies in magnitude because of the variation in reluctance of the magnetic path between rotor and stator teeth and the coincidence of these together with the speed determined the frequency.

Round about this time we were receiving Hudson aircraft built by the Lockheed Aircraft Corporation, and, of course, the aforementioned units were not suitable for the American engines powering those airplanes. A new unit of similar output therefore was produced with the necessary American standard flange and drive shaft. This gave full output at 2,000 rpm and upwards, the frequency at 2,000 rpm being 866 cycles per second.

However, not all aircraft are multiengined and on single-engined airplanes no pads are available for the alternator if a d-c generator is fitted. To meet this situation, a range of dual machines was produced in which the d-c armature is on an extension of the rotor, and the one body carries the alternator stator and the d-c generator poles. Outputs were 500 watts alternating current plus 500 or 1,000 watts direct current, the variations being on the d-c side, the latter could be a plain shunt machine or one of the self-regulating type.

Recently two more alternators have made their appearance similar in construction to those already described but with increased outputs of 1,200 watts. One is suitable for British engines whereas the other is designed for American drives.

The latest design of dual machine is a combination of this 1,200-watt alternator and the 3,000-watt d-c generator. This unit incorporates all the improvements resulting from experience on previous generators. The d-c portion is a normal six-pole shunt machine with compensating windings, and the alternator half has already been described. Full output is obtained at a minimum speed of 3,000 rpm with air-blast cooling of 100 cubic feet per minute, the frequency at that speed being 1,200 cycles per second. The weight is 51 pounds.

Also under development are engine-driven alternators of 3,500 watts and 6,000 watts capacity, the weight of the former being about 35 pounds while that of the latter is unknown.

In the field of alternating current for aircraft power supplies, the only installation at the moment does not derive the power from an engine-driven alternator through a constant-speed device, but rather from auxiliary generating plants. The system is 110 volts, three phase, 250 cycles, but it is believed that this voltage and frequency have not been definitely frozen for future projects, and it is hoped that standardization with American practice may be achieved. Two units are installed in the aircraft, independent of one another, one being used while the other is a standby. The weight of each is 450 pounds complete. Each alternator is rated 20 kw at 3,750 rpm, and a d-c generator of three kilowatts at 28 volts and an exciter of 750 watts are included in each unit. Some development is going forward with constant-speed devices, an eddy-current coupling being one approach, but the weight of this device is excessive. Another approach is a slipping device with tapered rollers, but no information is available on the progress of this project.

Requirements for Low-Voltage Aircraft Cable

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Synopsis: This paper presents the basic requirements to be expected of any electric cable which is considered for use in aircraft. A few of the problems encountered in forming a cable specification are presented, along with factors to be considered in establishing valid test procedures. The author has not attempted to give solutions to the problems presented, but rather to acquaint the reader with the fact that a major problem does exist in designing an electric cable suitable for use in aircraft, and that many factors which are extremely variable must be considered before an adequate solution can be found.

ELECTRIC wiring designed for aircraft probably is required to withstand a wider range of operating conditions than most other types of electric wiring. This is not surprising since present airplanes, both combat and commercial types, are expected to be able to fly anywhere in the world under all kinds of weather conditions. Repair of a damaged airplane in remote parts of the world must be accomplished with a minimum of replacement parts, and probably only basic tools, such as screw driver, pliers, hammer, and wrench. In many cases repairs can be made only by salvaging parts from other airplanes. If the original electric wiring of an airplane is properly designed, installed, and protected against damage due to overloads, short circuits, insulation failure, abrasion, fluids, and fungus growth, and so forth, a great deal of wire replacement and maintenance will have been eliminated in both combat and commercial types of aircraft, although in combat airplanes the larger percentage of wire replacement is caused by gunfire damage.

The following basic requirements are to be expected of any electric wiring considered for use in aircraft:

1. *Minimum Weight.* Unnecessary weight is expensive in an airplane because it results in a reduction of the performance characteristics and the range. A one-pound increase in the weight of an airplane when empty easily may result in three pounds increase in the gross weight because of the weight of additional structure and fuel needed to carry it. This shows that a considerable reduction of "pay load" results when additional structure and fuel are utilized to carry unnecessary equipment weight.

2. *Minimum Diameter.* Excessive wire diameter reflects itself in extra weight. Bundles of wire with excessive diameter will necessitate larger clips to hold them in place. Routing enlarged bundles through the structure may mean an increase in the size of openings through stressed portions of the airplane and result in weakening the structure unless an additional thickness of metal is used around the openings which have been enlarged. The additional strengthening material means added weight. If the diameter of the bare stranded conductor is larger than necessary, the diameter of the shank of the terminal lugs will have to be increased to accommodate the enlarged wire. An oversize in conductor diameter means that more insulation material and braid will be necessary to cover the wire. The size of the "insulation-grip" type of terminal, sometimes used on smaller-gauge wires, also will be increased. With the realization that most airplanes carry several thousand feet of electric wiring, and some of the larger transport airplanes carry several miles of electric wiring, it is easy to see that a small increase in size in the individual conductor can result in an appreciable increase in the weight of the finished airplane.

3. *Noninflammability.* The insulation on electric cable can be ignited by either a sustained short circuit or a flame external to the wire. If the insulation is not self-extinguishing, the burning wire becomes extremely hazardous, since it may propagate the flame from one point in the airplane to another, and it can ignite other inflammable materials and be the cause of a major fire. This dangerous situation has been intensified in the present-day combat and cargo types of airplane, because of the use of open wiring throughout the airplane. The insulation on wiring should be self-extinguishing.

4. *Wide Temperature Limits.* Wire and its insulation should show no signs of failure when used in ambient temperatures between +125 and −70 degrees centigrade. A military airplane used in Arctic regions today may be transferred to equatorial regions next week. For maintenance the wire must be capable of being bent and flexed at all temperatures without damage to the primary insulation. For operation the wire must be capable of standing the ovenlike heat in the engine section of an airplane which has just returned from a flight and is allowed to stand in a blazing tropical sunlight while the engine "cools off."

5. *Abrasion Resistance.* The braid covering of an aircraft wire must be capable of withstanding a great deal of abuse in installation and service. During installation and assembly, wiring is subjected to being pulled through small openings and across sharp metal edges, and to general rough shop treatment. In service-vibration and airplane motion may cause the loosely bundled open wiring to beat itself against a piece of structure or chafe against the sides of an opening through which the wire passes.

6. *Fluid Resistance.* Engine oil, hydraulic fluid, high-octane gasoline, salt water, alcohol, ethylene glycol, and any other fluid likely to be encountered should have no effect upon the outer protective coating of the wire. Open wiring in the engine nacelles, wheel wells, and other exposed sections is subject to all of the aforementioned fluids during operation and maintenance and therefore should not be materially damaged by exposure to any or all of them.

7. *Fungus Resistance.* This property is most important for airplanes used in tropical areas where high heat can be accompanied by high humidity. Wiring inside equipment and in confined parts of the fuselage may be attacked by fungus which will cause destruction of wire insulation and require early replacement.

8. *Maximum Current Capacity.* In a 28.5-volt d-c electric system the maximum current-carrying capacity is limited in almost all cases by the maximum allowable line-voltage drop rather than the rating of the wire. The allowable line-voltage drop has been set by specification and is dependent upon the operating requirements of the equipment. In the larger airplanes almost every circuit is carrying only a fraction of the current it is capable of handling because of the voltage-drop limit. A few circuits do carry their rated current, but in these isolated cases the length of wire between the main bus and the apparatus is very short, and the wire capacity can be utilized without exceeding the voltage-drop limit.

9. *High Dielectric Strength.* Within the range of operation of present-day aircraft, pressure altitude and relative humidity changes have no measurable effect upon the dielectric strength of wiring insulation. Pressure altitudes varying from sea level to 50,000 feet, and relative humidity from 0 to 100 per cent are encountered. The test voltage of from 2,000 to 5,000 volts which the finished cable is required to withstand when passing through a chain-electrode spark-test machine is for the purpose of discovering flaws in the insulation material, such as a thin-walled section of insulation, a small chip of metal in the material, an air bubble, or any other flaw which might cause mechanical or electrical insulation failure during installation and service. Low test voltages of a magnitude approximating those maximums that actually may be encountered would not be sufficient for revealing hazardous insulation flaws.

Establishment of a Specification for Aircraft Cable

In order that the wire manufacturers might have a requirement basis upon which the design and construction of an acceptable aircraft cable could be based,

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attempts have been made to formulate a specification for low-voltage aircraft electric cable. To aid in the determination of a specification suitable to the Army and Navy, the aircraft industry, and the wire manufacturers, the Technical Advisory Committee on Aircraft Cable (TACAC) has been established, whose membership includes representatives of these organizations. A specification acceptable to all concerned has been difficult to achieve, since each group has different problems and ideas concerning the same subject. The present specification resulting from extensive investigation is an Army–Navy Aeronautical Specification *AN-J-C*-48.

1. *Performance Specification.* This type of specification presents the performance requirements to be met by a manufacturer's product. Specific test procedures are presented which are considered valid tests for determining whether or not a product is acceptable. Within certain limits, determined by production limitations, design requirements, and specific uses, the manufacturer has considerable freedom in the design and construction of electric cable. Any suitable material may be used, provided the performance requirements are met. This is the most desirable type of specification if the minimum requirements are set high enough, since it encourages the manufacturer to develop new materials and methods of manufacture.

2. *Manufacturing Specification.* This type of specification is the most rigid of all, since it specifies how a product is to be designed, what materials are to be used, and what manufacturing process is to be used. Very little deviation by the manufacturer is tolerated. This type of specification should be avoided unless absolutely necessary, since it does not encourage design development on the part of the manufacturer.

Very few specifications can be classified entirely as one of the preceding types, since most of them are a combination of both types. Wire specifications are mainly of the performance type. In some instances, however, desired wire characteristics can be attained only by use of one known material or manufacturing process, and therefore use of that material or process must be made mandatory. In other instances, several materials capable of yielding the desired characteristics, if properly used, may be known, but it may not be feasible to set up a test procedure which will validly compare the relative merits of the several known materials. In this case, the use of a particular material may be made mandatory until a test procedure is established which will weigh the relative merits of the several materials.

Establishment of Valid Test Procedures

In writing a specification for low-voltage aircraft wire, stating the qualities that a wire should have in order to meet the specification is a relatively easy matter. The most difficult part is the establishment of test procedures which will give a valid measure of whether or not a particular type of wire will have the desired qualities and will be satisfactory for use in aircraft. For every type of qualification test, a large number of variable factors must be considered. Controlled laboratory tests and procedures should be designed to reproduce conditions to which the wires will be subjected during installation and service.

1. To establish a valid test procedure for determination of the flammability of wire the following factors are important:

(*a*). Wiring position is an uncontrolled variable, but is an important variable insofar as fire and flame rate of travel is concerned. Wire insulation will burn more rapidly, and flame will travel more quickly when the wire is held in a vertical position, or at some angle of slope than it will when held in a horizontal position, because the fire will tend to preheat the insulation above the point which is actually burning, and therefore aid in propagating the flame. A large portion of aircraft open wiring is in some position other than horizontal. Because wiring offers a means for carrying flame from one point to another, the flame rate of travel for the worst condition of wire position should be very small. The insulation should be self-extinguishing.

(*b*). Wire size is important in establishing a flammability test. A small wire will burn more readily than will a large wire. The insulation on an *AN*-0 wire will be more difficult to ignite than will the insulation on an *AN*-18 wire, because of the larger mass of copper which will dissipate the heat more rapidly. An *AN*-0 wire may not continue burning, because of heat dissipation by conduction through the copper, but an *AN*-18 wire may sustain or propagate combustion for quite some time.

(*c*). Small wires are loosely bundled together in open wiring systems. If one wire burns, it may ignite others and cause more rapid spread of the fire.

2. In establishing an abrasion test for wire, it becomes difficult to establish limits for the variables, since each organization will have its own requirements regarding the type and amount of abrasion the wire will have to withstand during installation and service. Installation problems and procedures differ so much that an abrasion-resistant wire acceptable to one concern may not be acceptable to another. Different types of abrasion are encountered in installation and service, namely:

(*a*). A cutting type of abrasion, due possibly to a wire resting on the edge of a piece of metal and allowed to move in a direction which would cut the insulation and braid.

(*b*). A scraping type of abrasion, encountered during installation and maintenance when wiring is pulled through the airplane structure.

(*c*). A pounding type of abrasion, caused by airplane vibration which makes the wire beat itself against structure.

All types will be subjected to varying degrees of intensity. The speed of the scraping action, the sharpness of the cutting edge, the weight of the wire or wire bundle, the frequency of impact, among other variables all will vary between wide limits. The difficulty in setting up a procedure to test for all of these variables can be appreciated. The problem is complicated further by the use of different materials in wire manufacture. Comparing abrasion resistances of cotton-braided, glass-braided, and rayon-braided wires is complicated by the use of different impregnating materials and protective coatings. A rayon or glass braid, in general, is inferior to a cotton braid on the conventional scrape-type abrasion-test machine if the same type of protective coating is used. However, by use of the proper impregnating material or coating, a rayon- or glass-braided wire can be made to appear superior to cotton-braided wire on the same abrasion test machine, and yet inferior on another test apparatus which utilizes a different means for abrading the wire. A definite need is indicated for an abrasion test, and suitable apparatus which will compare truthfully the different types of wire on a basis which will account for variables encountered in usage and will give consistent results regardless of material used.

To enumerate the factors affecting all types of specification tests would take considerable more space and time than has been allotted for the coverage of so vast a subject. It is sufficient to say that the problems peculiar to aircraft are many and varied, but are of the utmost importance in assuring a safe, dependable airplane, capable of operation in any part of the world with a minimum of maintenance.

Conclusions

By means of this paper, the author has attempted to show that a major problem of design has been given to the wire manufacturers by asking for an electric cable which will not fail in operation under conditions of:

1. Ambient temperatures from −70 to +125 degrees centigrade.
2. Relative humidities from 0 to 100 per cent.
3. Pressure altitudes from sea level to 50,000 feet.

In addition, the cable is required to be noninflammable, abrasion-resistant, fluid-resistant, and fungus-resistant, and yet still possess minimum weight, minimum diameter, maximum current-carrying capacity, and good dielectric qualities. A similar problem also has been given to the committees and boards which determine the specification test procedures for giving a valid measurement of the previously stated requirements.

The wire manufacturers, the armed services, the working committees, the aircraft manufacturers, and all others concerned with the problem of design, manufacturer, and use of electric cable have done an excellent job in establishing and maintaining the quality of wire that is being used in present-day aircraft. Their efforts are rewarded by the thousands of airplanes which fly daily with a minimum of failure in the electric systems due to causes other than gunfire. Many important problems, however, are yet to be solved, and their ultimate solution will depend mainly on the continued and coordinated effort of all organizations concerned.

Requirements for Aircraft Electric Motors

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DESIGN requirements for electric motors intended for use on aircraft may be classified roughly into performance and construction. These two classifications are not completely independent of one another, as some of the construction requirements will have pronounced effect on the performance characteristics. Performance characteristics may be subdivided into the following headings:

(*a*). Operating voltages.
(*b*). Torque requirements.
(*c*). Rpm limits.
(*d*). Duty cycle.
(*e*). Efficiency.
(*f*). Ambient-temperature conditions.
(*g*). Protection against overheating.
(*h*). Radio-noise output.

Construction requirements may be subdivided into the following headings:

(*a*). Resistance to vibration and accelerations.
(*b*). Corrosion protection, sand and dust resistance.
(*c*). Gearboxes, clutches, and brakes.
(*d*). Types of bearings and lubrication.
(*e*). Limit switches.
(*f*). Provision for emergency hand operation.
(*g*). Method of mounting and bringing out leads.
(*h*). Maximum over-all dimensions.
(*i*). Maximum allowable weight.
(*j*). Maintenance considerations.

The manner in which the preceding requirements are established and the specific reasons for some of the requirements will be outlined briefly in the following paragraphs with particular reference to 24-volt d-c motor applications.

Operating Voltages

Normal operating voltage at the motor is considered to be 27 volts for continuous-duty motors and 26 volts for intermittent-duty motors. In case of failure of the main generators, the voltage under any appreciable load may fall as low as 20 volts, and satisfactory emergency operation of some motors may be specified at this low voltage. Improper setting or malfunctioning of the voltage regulators may permit the line voltage to rise as high as 30 volts occasionally, and the overspeeding thus created must not impair operation nor running balance of the motor. All parts of the motor must be designed to absorb the torque and momentum loads imposed by sudden stoppage of the armature due to jamming of the driven load at the highest service voltage without damage or loosening of parts.

Torque Requirements

For unlimited rotation applications the starting torque required is usually determined by the type of load to which the motor is connected. Motors driving hydraulic pumps and similar accessories are usually required to supply full-load torque at starting. Motors driving fans, blowers, generators, and similar accessories normally require only a fraction of full-load torque for starting. To provide a margin of safety for the higher frictions encountered at low temperatures, shunt- and compound-wound motors should have a minimum starting torque of 175 per cent normal running torque, and series-wound motors should have a minimum starting torque of 300 per cent normal running torque.[1] Normal running torque is defined as the torque corresponding to the horsepower and the rpm appearing on the nameplate.

Many aircraft motors have a load which is not constant, so that continuous torque ratings are meaningless. This is particularly true for linkages employing toggles where the force required increases rapidly at the overcenter position and then decreases. The first step in applications of this type is a thorough study of the load to be operated, by plotting the torque required versus either time or the percentage of operating cycle. Total torque which the motor will be required to deliver at any time consists of the weight loads for ground operation plus maximum air loads due to flight plus any additional loading due to ice accumulations on exposed surfaces, and increased friction due to cold grease, dust, and differential contraction of metallic members.

With the loading curve in Figure 1 the values of maximum torque required and the points at which they occur may be determined. By introducing the maximum time permissible for completion of the cycle and the number of cycles required per hour, the duty cycle and the intermittent horsepower rating required can be established. If a fixed percentage of the cycle need not be completed per unit of time, the varying speed characteristics of a series-wound motor may be used to obtain a reduction in motor weight and to simplify the control required.

Rpm Limits

In the interest of weight reduction, the motor speed must be as high as possible. The upper limit of continuous speed at present is about 12,000 rpm, this limit being imposed by satisfactory ball-bearing operating and life. On larger sizes of d-c motors, the upper limit of speed may be established by the danger of the armature windings and commutator bars being thrown out of position by centrifugal force.

Duty Cycle

Duty cycle is usually specified as so many minutes "on" followed by a certain number of minutes "off." Given the number of complete operating cycles required per hour and using Figure 1 as a basis, the duty cycle of the motor can be established, enabling selection of the lightest motor which will power the application without dangerous overheating. In calculating the duty cycle, allowance must be made for those applications where a large number of short runs, that is, "inching" toward the desired setting, are common, as in wing-flap or trim-tab positioning service. Applications of this kind impose a much more severe duty cycle on the motor than running continuously from start to finish of the operating cycle. Open-type self-ventilated motors save weight for continuous-duty applications, but provide little or no weight savings on highly intermittent-duty applications. In general, an intermittent-duty motor is lighter than a continuous-duty motor of the same rating.

Efficiency

Minimum allowable efficiencies for 24-volt d-c motors from 0.02 horsepower to 4.0 horsepower are specified in Army–Navy specification *AN-M*-10.[1] While these values are intended for military aircraft and may, at first analysis, appear to be unnecessarily low, they represent a sacrifice of efficiency to obtain a lower motor weight. Higher efficiency could be obtained by increasing the amount of copper and iron in the motor

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but usually cannot be justified, because in addition to the weight of the motor, the weight of the extra gasoline required to carry the heavier motor also must be charged against the more efficient design. High efficiency is of much more importance in continously operated motors than in intermittently operated motors.

Ambient Conditions

Operating temperatures for motors mounted in exposed locations on the airplane range from -65 degrees centigrade to $+70$ degrees centigrade,[1] and this complete range may be encountered within an hour. Location in cabins or other semiprotected locations does not necessarily mean more favorable ambient temperatures, since airplanes are frequently stationed at operating bases where hangers are not available.

Ambient atmospheric pressures vary from 14.7 pounds per square inch at sea level to 2.72 pounds per square inch at 40,000-feet altitude. This reduction in pressure, although slightly offset by the reduction in temperature as the altitude increases, results in decreased weight of air per cubic foot and forced cooling based on a definite number of cubic feet of air per minute will be inadequate unless the necessary allowance is made in calculating cooling air available. Motors located in supercharged compartments ordinarily will not be subject to ambient pressures lower than 9.5 pounds per square inch. Creepage distances satisfactory at sea level must be increased 300 per cent for satisfactory operation at 40,000 feet.

Ambient humidities will vary from 0 to 100 per cent. Service reports from the South Pacific areas disclose that the humid warm climate is particularly conducive to the creation and rapid development of fungus growth on textiles and all other organic materials, including those used for electrical insulation, unless prevented by a fungi retardant included in the insulating varnish or applied over the varnish. Under ideal conditions for growth, enough fungi will develop in one week to fill completely the air gap between armature and pole shoes and prevent the armature from turning.

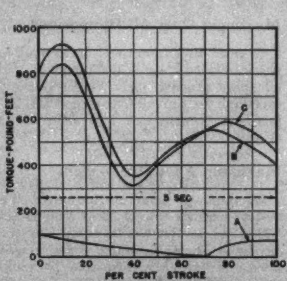

Figure 1. Torque versus per cent stroke for typical aircraft application

A shows torque required for weight of door
B shows torque required to overcome air loads on door
C shows total torque required from the motor

Motors located in wings or other compartments where explosive mixture of gasoline vapor and air may accumulate must be provided with suitable explosion-proof housings and have excellent electrical bonding between all parts of the motor enclosure and the supporting structure to eliminate any possibility of igniting such mixtures by an electrical discharge between the motor and structure. The resistance between any two metallic components of the motor and between the motor base and the airplane structure must not exceed 0.002 ohm.

Protection Against Overheating

The question of whether or not thermal protective devices should be required on all motors is very controversial at the present time. For critical circuits, such as propeller feathering, it is obviously unwise to include a "protective" device which will prevent operation of the motor just because it is five degrees above the established safe temperature of the motor windings when unrestricted operation of the feathering pump motor, even though it be severely damaged in the process, would eliminate a dangerous flight condition. On other motors not supplying "time-critical" functions, such as transfer pumps and cabin superchargers, built-in thermal switches protect the motor against unintentional or unsuspected overloads by cutting the motor off the line until it has cooled to a safe temperature. The cycling of the motor due to automatic reclosing of the protective device gives warning to the crew that something is wrong with either the motor or its load, and they may then either clear the trouble in flight or cut the circuit off the line for repair at the next ground stop.

Radio-Noise Output

Greatly increased amount of radio equipment now being used on military airplanes, together with the greater receiver sensitivity and nonshielded wiring installations, have made minimizing of radio-noise voltages imperative. Radio-noise voltages may be generated by d-c motors because of poor commutation, poor combination of stator and rotor slots, and straight slot rotors. The high-frequency voltage ripples may travel either by direct radiation or by conduction along the primary power lines to emerge eventually as an unbearable noise in the radio-receiver headphones. In addition to poor commutation due to improper design, poor seating of the brushes on the commutator has proved a prolific source of radio noise. On a one-eighth-horsepower 1,750-rpm 24-volt-d-c pump motor, for example, the radio-noise voltages measured as shown in Table I.

Table I

Frequency (Kilocycles)	Motor Load	Microvolts on Motor Leads	Radiation Noise at Four-Foot Distance
200	No load	45,000	1.5
200	50 per cent	200,000	1.0
200	Full load	200,000	1.6
600	No load	20,000	12
600	50 per cent	30,000	16
600	Full load	25,000	22
1,000	No load	8,000	1.0
1,000	50 per cent	13,000	1.3
1,000	Full load	21,000	2.0
10,000	No load	70	1.0
10,000	50 per cent	80	1.1
10,000	Full load	100	1.2
20,000	No load	10	1.1
20,000	50 per cent	12	1.2
20,000	Full load	13	1.2

On disassembly the brushes were found to have only 25 per cent area of contact with the commutator. After seating the brushes properly and cleaning the smut from the commutator surface, the radio-noise voltage dropped to 50 microvolts at 200 kilocycles per second and correspondingly less at all the higher frequencies. The radiated-noise field disappeared completely after this treatment.

From a weight standpoint, it is not efficient to apply noise filters to all rotating electric equipment, and, therefore, it becomes necessary to minimize the noise each item of equipment produces and to supply a power-line filter just ahead of the radio receivers.

Resistance to Vibration and Acceleration

Although the terms are often used interchangeably, acceleration and vibration are not the same. Vibration always gives rise to acceleration, but acceleration can exist without vibration. The general formula for acceleration is:

$$\frac{a}{g}=\frac{(2\pi f)^2r}{386}.$$

where:

$\frac{a}{g}$ = number of g's acceleration in feet per second per second

f = frequency in cycles per second

r = peak amplitude in inches (one-half the total excursion)

Values of acceleration and vibration vary widely from one type of airplane to another, and even in different sections of the same airplane. The problem is further aggravated by motor mountings which may be flexible enough to permit resonant vibration, in which case the acceleration may increase as much as 20 times. If resonance is not present, ability to withstand accelerations of 12*g* applied at the center of gravity in any direction generally will be satisfactory for aircraft applications, including the acceleration value obtained by landing impact.

Table II indicates the range of vibration and acceleration encountered on a typical twin-engine medium bomber.

Corrosion Protection, Sand and Dust Resistance

Use of light metals, such as aluminum and magnesium, to reduce weight has aggravated the corrosion problem, since steel, copper and other metals lower in the galvanic series in sea water also must be used in the motor, often in intimate contact. Effective and durable protective finishes must be provided between dissimilar metals; otherwise any condensation or salt spray will form an electrolyte, and the aluminum and magnesium will be destroyed rapidly by galvanic action. Severe condensation occurs when a descent is made from the cold air of high altitudes into the warmer air near the ground.

Global operation subjects motors to fine sand and dirt. Given sufficient time, this sand eventually will penetrate any seal except the hermetic type. These abrasive particles rapidly grind away bearings, brushes, commutators, switches, and gears once they have gained entrance. The nearest approach to a solution at present is to design as effective a seal as possible and to keep the seal effective by periodic cleaning or replacement.

Gearboxes, Clutches, and Brakes

Gears for aircraft motor applications present a difficult design problem because of the small weight and space allowed for them and the difficulty of providing satisfactory lubrication over the wide range of temperatures encountered. Sintered iron gears impregnated with oil have given satisfactory service on fractional horsepower motors. Hardened steel gears with low-temperature grease have been fairly satisfactory for larger geared motors, but no low-temperature grease yet available is entirely satisfactory at high temperatures. Gears must be capable of absorbing the shock caused by the jamming of the driven mechanism at maximum motor voltage without damage or breakage.

Clutches are specified when the momentum of the armature must be removed from the load to prevent overshooting of the output shaft when the brake is applied, or when the frictional load of the armature and its associated gearing is to be removed from the system to facilitate hand-crank operation.

Bearings and Lubrication

Bearing sizes used on commercial motors are too small to withstand the loads and vibrations encountered in aircraft applications.

In general, ball bearings have proved more satisfactory in service than sleeve bearings, and sealed-type ball bearings are preferred, particularly for the higher speeds. Bearings must be designed to take thrust from both directions. However, because of procurement difficulties, sleeve bearings must be substituted for ball bearings when adequate supplies of the latter are not available. Oil-impregnated bearings have not proved too satisfactory. Excessive lubricant must be avoided. The type of lubricant must be such that it will provide lubrication at the extreme high and low temperatures encountered. The lubricant should not become watery at the high temperatures or go solid at the low temperatures. At present, lubricant in accordance with Army–Navy specification *AN-G-3*[2] is the most suitable for meeting these conditions. Enough lubricant should be provided for 500 hours of operation without attention, with the motor operating in any position.

Limit Switches

To provide the utmost reliability of operation, limit switches should be mounted on the mechanism being operated. In order to simplify wiring, however, limit switches are usually incorporated in the driving motor. Switches used for this purpose must be extremely resistant to vibration and must have adequate current and voltage-breaking capacity both at sea level and at high altitudes. Snap-action contacts are necessary and compactness is important. Means of adjusting the setting of the limit switches from outside the motor must be provided and the setting adjustment locked against changes due to vibration or continued operation of the motor.

Table II

Location	Total Excursion (Inches)	Frequency (Cycles Per Second)	Acceleration (Ft/Sec/Sec)	Maximum Acceleration at Resonance
Engine accessory case	0.020	50	2.56	
Engine accessory case	0.006	185	10.5	40
Fuselage nose structure	0.007	148	8.0	18
Fuselage cabin structure	0.002	148	2.0	40

Provision for Emergency Hand Operations

Some applications, such as landing-gear actuators, require that means for hand operation be provided in case of failure of the electric system for any reason. The hand-crank extension should not turn when the motor is driving the mechanism, nor should it be necessary to turn the motor armature when operating the mechanism with the hand crank. The torque required to turn the hand crank should be low enough so that one man can turn the crank continuously without requiring the advantage of his full weight. Each application of this kind requires individual consideration to establish the most suitable design.

Method of Mounting and Bringing Out Leads

Type of mounting is determined by the individual application. Flange mountings are lighter than base mountings, but are subject to high stresses due to bending moments, particularly if resonant vibration is encountered. Flange mountings must provide sufficient space to permit insertion and removal of bolts or nuts and adequate wrench clearance for tightening them. Tapped holes directly in aluminum are not satisfactory, and such applications should employ plated-steel inserts.

Leads for small motors not subject to severe vibration should consist of multiple pin receptacle in accordance with Army–Navy specification *AN-W-C-591*.[3] Leads for large motors where suitable *AN* connectors are not available, and for all motors on which severe vibration makes *AN* connectors unsatisfactory, terminal studs should be provided. Terminal studs must be anchored securely against turning. Leads inside motors must be anchored firmly and protected against abrasion.

Maximum Over-All Dimensions

Over-all dimensions must be as small as possible, since space is at a premium, and all space possible must be left around each unit to provide for inspection, adjustment, ventilation, and removal as necessary.

Maximum Allowable Weight

Every pound saved on the weight of a motor permits more than one-pound additional pay load to be carried by the airplane. The value of one-pound weight saving is variously estimated by commercial airlines at from $100 to $1,000 per year. In military airplanes no momentary value can be placed on the ability to carry a few more rounds of ammunition or extra gasoline, but the objective is obviously very important. Motor designs must not penalize reli-

Functional Design of Aircraft Electric Actuator Equipment

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IN their order of importance the factors which influence the design of an electric actuator for use on aircraft are:

A. Dependability under extreme operating conditions of:

1. Vibration.
2. Acceleration.
3. Temperature.
4. Altitude.
5. Humidity.

B. Performance of intended function with regard to:

1. Speed of operation.
2. Accuracy of movement.
3. Duty cycle.

C. Safety of operation is provided by:

1. Emergency operation provisions.
2. Avoidance of inadvertent operation.
3. Avoidance of fire hazard.

D. Weight influenced by:

1. General schematic arrangement.
2. Choice of materials.
3. Treatment of materials.
4. Detail design of parts.

E. Availability of component parts influenced by:

1. Importance of application.
2. Production schedule.

F. Cost as a function of:

1. First cost in money influenced by:
(*a*). Simplicity of design.
(*b*). Availability of materials, parts, and facilities.
(*c*). Quantity of units to be produced and production schedule.
2. Cost of maintaining the equipment:
(*a*). Money spent throughout life of the actuator.
(*b*). Flight time lost because of maintenance.
3. Cost in loss of performance due to weight.
4. Cost in loss of pay load due to weight.

The purpose of this paper is not to give detailed instructions, but only to point out to the designer of aircraft electric actuating equipment the points which must be considered in formulating such a design and to indicate the methods of obtaining solutions to the many problems involved. The design of equipment of this nature will be found to be about one-fourth electrical and three-fourth mechanical engineering and should only be attempted by an engineer or group of engineers with broad training and experience in both fields.

Electric actuators on aircraft are used to perform functions such as retracting and extending the landing gear, extending and retracting landing and maneuvering flaps, controlling engine cooling air flow, adjusting control surface trim tabs, handling cargo and bombs, and for many other services. From a study of these applications it is seen that the actuator must be dependable and give satisfactory operation under the conditions encountered in aircraft operation. The actuator will be subjected to vibrations at fundamental and harmonic frequencies to 200 cycles per second at amplitudes that produce accelerations to 20 gravity. Most actuators must be installed without shock insulating mounts and therefore of necessity will be rugged in construction with the electrical control balanced to avoid inadvertent operation of mechanism elements or electrical contacts. The actuator and its control should be suitably designed to operate in ambient temperatures of −65 to +160 degrees Fahrenheit at any altitude to the service ceiling of the aircraft and under all conditions of relative humidity to 100 per cent saturation. This will mean that particular attention must be paid to the selection of lubricants, oil seals, breathing provisions, and corrosion preventing treatments. At altitudes above 25,000 feet the brush life of d-c machines will be materially decreased, and it will be necessary to use especially treated brushes in units that are required to operate for any appreciable percentage of the time at these altitudes. Another point to be considered in high-altitude operations is that the breakdown voltage for a given air gap or creepage is very considerably lower than at sea level.[1] Some investigators have attempted to show that at extreme altitudes the cosmic rays or other radiations have an effect on the electrical performance of contacts and air gaps.

In order to design an electric actuator that will perform satisfactorily it will be necessary to have accurate data concerning the direction and magnitude of the forces required of the unit at all portions of its movement, the speeds required at all points, and the frequency in time at which this movement will be required. With this information and with some knowledge of the actuator mechanism, it will be possible tentatively to select a motor with the proper speed–torque characteristics and to design a suitable electric control system. The accuracy with which the actuator is required to position its mechanical output will be dictated by the type of application and by the mechanical means used to connect the actuator to its load. The most widely used device is the limit switch which is used in conjunction with an electric brake

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ability of operation to save weight, but if considerable weight can be saved by shortening the useful life without affecting the reliability during the shorter life, the saving in weight will justify a higher cost for the lighter motor.

Maintenance Considerations

Every airplane which is not in flight nor immediately available for flight is a liability instead of an asset. Ninety per cent of "the airplane not available" time on the ground is required for maintenance work, with poor weather accounting for the remaining time. Designs which facilitate inspection and maintenance obviously will increase airplane availability.

Of necessity, military maintenance and repairs often are performed at poorly equipped temporary bases by inexperienced personnel outfitted with only the simplest of tools. Consequently, aircraft electric motors must be as simple, rugged, and easy to assemble or disassemble as the designer's ingenuity can make it.

Design features which permit rapid and efficient maintenance of electric motors include the following:

(*a*). Lubrication points clearly marked.
(*b*). Brushes readily removable for inspection and replacement.
(*c*). Spare parts exactly interchangeable without requiring reworking.
(*d*). Army/Navy standard bolts, nuts, and screw threads throughout.
(*e*). Complete identification on a permanent nameplate.
(*f*). Readily removable inspection covers.
(*g*). External adjustments for limit switches, slip clutches, and so forth.

In addition, a complete set of service instructions should be supplied to the aircraft manufacturer, showing sectional views of the motor, detailed disassembly and reassembly procedure, servicing data, spare-parts catalogue, and so forth, so that all these data may be included in the *Erection and Maintenance Manual* supplied with each airplane.

References

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2. Army–Navy Aeronautical Specification *AN-G-3a*. GREASE, LOW-TEMPERATURE LUBRICATING, March 6, 1944.

3. Army–Navy Aeronautical Specification *AN-W-C-591*. CONNECTORS, ELECTRICAL AIRCRAFT, December 19, 1941.

on the motor. This device gives fairly accurate positioning which may be further improved by adding dynamic braking or plugging provisions to the control system. If extreme accuracy that will not vary with load, voltage, or temperature is required, it will be necessary to build into the mechanical part of the actuator provisions for mechanically limiting the motion and to absorb the kinetic energy of the moving system. In some applications of electric actuators, it may be necessary to stop the motion or cause reversal when the load increases or decreases beyond specified limits. This effect may be conveniently accomplished by measuring the reaction on a stationary ring gear of a planetary system of gearing and thereby obtaining a force that is proportional to the amount of torque being applied to the system. This force may be opposed by springs and the movement thus obtained may be used to operate control-circuit switches.

There are applications of electric actuators on aircraft such as landing gear, wing flaps, engine-cooling control, bomb release, fuel valve, and so forth, where it may be necessary because of safety requirements to provide for emergency operation in case of gun-fire damage or other failure of the mechanical or electrical parts of the actuator system. In the case of landing gear it is common practice to provide either cable control that will release the gear from its actuator and allow it to extend by gravity or to provide for hand cranking of the actuator. In some applications such as wing flaps and engine-cooling-air control it will not be necessary to provide an actual mechanical override on the actuator but it will be sufficient only to install an emergency switch that will by-pass the normal control system and feed power directly to the motors. In the case of electrically controlled bomb racks and fuel valves, it is often required that a direct mechanical override be provided by means of cables.

In nearly all applications of electric actuators it will be necessary to provide against inadvertent operation. Control and power relays should be either of the balanced type or mounted in such a position that a severe acceleration in any direction likely to be encountered will not cause the relay to change the position of its contacts. It will also be necessary to avoid the use of relays that have their natural vibration period within the range of frequencies to be encountered in use. The use of sensitive relays in aircraft control should be avoided if possible, but in cases where they must be used the designer should provide shock-insulating mounts and water- and dust-tight covers.

In cases where the consequences of inadvertent operation of some piece of electric equipment would be very serious, the switches at the control station should be protected with plainly labeled and distinctively colored switch guards. In extreme cases it may be desirable to install two or more control switches in series.

In applications that require, by automatic control, the actuation of several different but related parts of the airplane in a certain definite sequence, it will be necessary for the designer to consider the possibility that the actuator may be stopped or reversed at some point other than the end of a cycle. The control must be designed so that under these conditions the proper sequence of operation is maintained.

There are locations in aircraft where explosive concentrations of gasoline vapor may be present under either normal or damaged conditions and it is very important that this point be considered when designing an actuator system. Motors and control equipment should be located where there is adequate ventilation and where these vapors cannot accumulate, but if this is not possible a weight penalty must be accepted because of the necessary explosion-proof construction.

Weight considerations are of paramount importance in the design of equipment of this nature and must be considered when the electrical and the mechanical schematic diagrams are being made. These schematics should be very complete and at this stage it is advisable to determine all gear ratios, screw leads, speeds of all elements, and all loads. There is an opportunity in a design of this nature to do a great deal of original thinking and the making of the schematic diagrams is the point at which most of the nonroutine work is done.

It should not be attempted in a paper of this length to give detail gear-design procedure but there are several basic considerations that may be covered. In the design of aircraft electric actuators the gearing most likely to be required will be straight spur, internal, bevel, helical, and worm. In work of this class the usual standard gear-design practice is not adequate because of the extreme conditions imposed by weight limitations, space requirements, high speed, extreme ambient temperatures, duty cycles, and life requirements. In these designs it will be necessary to call on experience and testing to a very large extent. Gear blanks may be forged from many of the SAE steels but, because of wartime shortages and restrictions, it may not always be possible to obtain those which are most suitable. In the more highly stressed high-speed applications probably the most satisfactory steel will be a high nickel such as 2515 because of its ductility, toughness, and resistance to fatigue. This is a carburizing type of steel and the gear teeth should be finish ground after heat treatment. Other very satisfactory gear materials will be found to be some of the chrome-molybdenum and the chrome-nickel-molybdenum steels which may be hardened without carburizing. Heat-treated blanks of these materials may be completely machined and then have the wearing surfaces hardened by the high-frequency induction process. As this method of surface hardening will result in almost no distortion it is only necessary that the gears be lapped to obtain extreme accuracy.

In the design of gears for this class of service considerable weight may be saved if forged blanks are used because the controlled grain flow and the small chance of having cracks or inclusions at critical sections will allow much higher stresses to be used.

In the design of these lightweight high-speed gears the designer must call on experience and testing to a very large extent and not follow too closely the accepted practices used in general machinery or automotive design. The best tooth form for the comparatively small highly stressed high-speed gears used in an airplane electric actuator differs from all of the accepted standards in that the pinion has a long addendum tooth which mates with a gear having a correspondingly short addendum.

This is an involute system using a 20-degree pressure angle with a full-depth tooth and has a number of advantages over the American Standard 20 degrees stub-tooth system. By having unequal addendums on the mating gears it is possible to have a pinion with a smaller number of teeth without the necessity of undercutting for mating tooth clearance. As a result the tooth will be thick at the root and will have a greatly increased beam strength. Another advantage of this system of gearing is that being full depth, the arc of action will be greater than that of the 20-degree stub-tooth involute system with the result that smoother action and lower tooth impact loads will be obtained at high speeds. This unequal addendum system of gearing is being considered at present for adoption by the American Gear Manufacturers' Association and has received much favorable comment.

In mounting gears the accuracy of alignment and the rigidity of shafting are of the utmost importance. It is necessary to place very close tolerances on the pitch cylinder tangency of straight spur and of helical gearing and on the pitch-cone tangency of bevel gearing because any inaccuracies in this respect will reduce the effective widths of the gears and reduce the power-transmitting capacity. The problem of obtaining accurate gear position is further complicated by the fact that the bearings on each end of a gear shaft are usually mounted in different pieces of the gear-housing assembly. Another factor that affects the accuracy of tooth engagement is the gear-shaft deflection due to the separating forces set up by the tooth pressure acting at the 20-degrees pressure angle. It is necessary that this force be calculated for each engagement in the actuator gear train so that the lightest satisfactory shaft may be designed and also so that proper bearings may be selected and provided with suitable supports.

It is not considered practical in this class of work to provide for adjustment for straight spur or helical gearing because of the direction in which these adjustments would necessarily have to be made. When using straight or spiral bevel gearing it is usually advisable to back up the thrust bearings with an adjustment.

The practical limit on the face width for straight spur gearing will be found to be about four times the circular pitch and for bevel gearing about one third the slant height of the pitch cone. The reason for this limitation in face width is the difficulty in obtaining accurate full-width contact at the tooth engagement. However, if it is desirable because of space or other limitations to increase the power-transmitting capacity these face width limitations may be exceeded by using very accurately finished gears and by providing very accurate and rigid mountings.

Planetary systems of gearing are often used in aircraft actuator design because of the high reduction ratios and power-transmitting capacities which may be obtained in a small space and also because the gearing is symmetrical about the center line of the system. In gearing of this type it is common practice to use two or more planet gears in order to get the maximum power transmitting capacity for the minimum weight of gearing and gear case. When multiple planets are used, the gearing and mountings must be very accurate in order to distribute equally the load between these planets. One point that must be borne in mind by the designer of planetary gearing systems is that when two or more planet gears are used the sum of the number of teeth in the ring gear and in the sun gear must be divisible by the number of planet gears in the system. If this rule is not observed the various elements of the system will not mesh properly with each other.

Very good efficiencies may be obtained using either simple or compound systems of planetary gearing, but the high reduction differential planetary is to be avoided if any considerable amount of power is to be transmitted. Because of the high circulating power within the differential system, the over-all efficiency is likely to be too low to be practical. This circulating power may be calculated by analyzing the system to obtain the pressures and velocities at each engagement and will be found to be many times the amount of power being transmitted through the system. As the power lost at each engagement is nearly one per cent of that entering the engagement, the efficiency of the system may easily be determined.

In calculating the strength of gear teeth for use in aircraft electric actuators, the conventional method using the Lewis formula is not found to be suitable for gearing of the quality described here because the weight would be excessive. This empirical formula is very conservative and will result in calculated stresses that are approximately eight times as great as those which are actually obtained in these high-precision gears.

Careful design and testing will show that most of the gears in an actuator of this type may be made with a rather thin web between the hub and rim and that this section may have lightening holes. Another weight-saving measure that is often overlooked is the use of hollow gear shafting.

It will be found in most cases that antifriction types of bearings should be selected for moderate to heavy thrust loads and for pure radial loads above 500 rpm. The higher-speed elements of gearing will most likely have relatively high values of diametral pitch requiring a high degree of mounting accuracy which can only be obtained through the use of ball, roller, or needle bearings. Another point in favor of the antifriction type of bearing is that less trouble due to lowered efficiency will be experienced at extremely low temperatures. The bearings for intermediate stages of straight spur gearing may be of the needle or roller type with the shaft ends held against thrust only by brass disks. When using needle bearings it is necessary that the loads encountered cause only extremely low values of shaft deflection in order to properly apply these loads to the needles and prevent their becoming jammed. When using an aluminum or magnesium gear case with highly loaded ball or roller bearings, it is good practice to press a steel bushing into the recess and use this bushing to support the outer race of the bearing.

It is important in actuators for this class of service to use lubricants that will operate satisfactorily in ambient temperatures from −65 to +160 degrees Fahrenheit. This problem of lubrication is at present receiving a great deal of attention by various research organizations and it is expected that an entirely satisfactory answer will be found in the near future. Seals must be used that will prevent lubricant from being lost from the unit and that will keep dust and water from entering while at the same time permitting breathing with changes in altitude.

In designing an electric actuator for aircraft the availability of all component parts must be considered. If the project is of sufficient importance and time will permit, it will be feasible to develop new materials and processes for use in manufacturing the unit. In alloting time for design, research, and testing, the designer must consider the production schedule that must be followed and the manufacturing facilities that can be made available.

The actual design of the actuator unit will very largely determine the cost of the units in production because of the effect on tooling, procurement of materials, and manufacturing facilities required. If the quantity of units to be produced is large it may be advisable to adopt designs that will involve large expenditures in special dies, molds, and other tooling which would result in a lower over-all cost.

In the total cost of a piece of equipment of this nature the designer should consider the cost in maintaining the unit in airworthy condition throughout its expected life which, of course, will include the loss in revenue incurred by having the airplane out of service for repairs.

As shown by this paper a very great effort must be made by the designer of an aircraft electric actuator to cut the weight to an absolute minimum consistent with the stated requirements. It is also shown that this is accomplished by ingenious design by the proper selection of materials, by the rigid control of processes, and by the insistence of a high standard of workmanship. Every ounce of weight that can be saved shows an increase in aircraft performance in terms of speed, range, and pay load.

Reference

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A 40-Kva 400-Cycle Aircraft Alternator

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Synopsis: A-c auxiliary-power systems for aircraft date back to World War I. Recently numerous technical articles have indicated an intense revival of interest in this subject. Heart of any electric system is the generator which supplies electric power to all parts of the system. An alternator, especially designed and built for 208-volt three-phase systems is described in this paper. Light weight and reliability dominate the design, which involved a large number of new and unusual problems.

ALTERNATING current has replaced direct current in large commercial power systems because large amounts of power can be transmitted long distances more economically; alternating voltages can be stepped up or down at will by a transformer; and the polyphase squirrel-cage induction motor, which employs no brushes, is so simple and reliable. These advantages, which also apply in aircraft use, have caused a long-continued interest in a-c systems.[1,6] While this interest is particularly strong now, it is to be remembered that the use of a-c systems in aircraft dates back to World War I when wind-driven alternators with built-in spark gap were used to supply power for radio transmitters. More than ten years ago 600-watt alternators driven by a main engine through a constant-speed drive, were used by the Navy. Prior to the present war two different a-c systems were tried experimentally in different planes: one of these was a single-phase 800-cycle system, the other, a three-phase 120-volt 400-cycle system. Rectified a-c systems with 30-volt d-c outputs of 200–800 amperes have also been proposed.[3]

Vernon Grant and Melville Peters showed in 1939 that the optimum voltage for the electric system of airplanes of 20,000 pounds gross weight or larger exceeds the present universal standard of 28 volts.[10] Because higher system voltages are desirable for large airplanes, alternating current appears preferable to direct current for high-altitude operation. Alternating current is easier to interrupt; moreover, the motors have no brushes and are generally lighter and more reliable. A-c power can supply 85 per cent of the total load and, in addition, it has many advantages as a power supply for aircraft radio.[9]

Weight, of course, is always a primary consideration in aircraft equipment. At a meeting of the AIEE Dayton Section in May 1944 an interesting tabulation of comparative weights of different electric systems was presented. The weight comparison was made for a hypothetical bomber of assumed dimensions, and an assumed gross weight of 150,000 pounds. This comparison is given in Table I.

Viewed in any light, the advantages of alternating current are so great that the development of the alternator described in this paper was initiated.

Alternator Rating and Requirements

Alternators of any desired rating can be built for aircraft service. This paper however, is limited specifically to the description of an alternator having a continuous rating of 40 kva, 208 volts, three-phase, 400 cycles, 75 per cent power factor. Overload ratings for this alternator are as follows:

60 kva at 100 per cent of rated voltage for five minutes.

80 kva at 90 per cent of rated voltage for five seconds.

From a heating standpoint, 150 per cent load for five minutes is a more severe requirement than full load continuously, but overload capacity, of course, is necessary to meet emergency conditions. The 80-kva rating was set up as a requirement for two reasons: to provide capacity for starting a number of motors simultaneously, and to ensure greater stability when carrying a load of 60 kva. These load ratings are based upon 250 cubic feet per minute of cooling air being supplied from a blast tube at not less than six inches water total pressure. An internal fan has been built into this alternator to provide cooling sufficient for a small amount of output—about 25 per cent of rated load—while operating on the ground without the benefit of blast cooling. The alternator is designed to supply a three-phase system with grounded neutral. Six leads are brought out to permit the use of differential protection, which disconnects the alternator from the system in the event of a short circuit, either at its terminals or inside the alternator.

Leads are tagged according to American Standards. *T*1, *T*2, *T*3 are line leads, whereas *T*4, *T*5, *T*6 are neutral leads. Alternator and exciter connections are shown in Figure 10. The terminal block is stepped to facilitate wiring connections and to avoid interference of leads. Without the stepped arrangement, the leads easily could be short-circuited at the block. A cover is provided for the terminal block to protect the leads and terminals.

Damper or amortisseur windings are incorporated to provide stability when synchronizing and when operating in parallel with other machines.

Careful consideration was given to selection of the operating speed, and 6,000 rpm finally was selected as the highest speed feasible for a machine of 40 kva. But the machine is designed to withstand an overspeed requirement of successful mechanical operation at 9,000 rpm, to correspond with the maximum speed of the drive shaft.

Alternator Size and Mounting Arrangement

All important outline dimensions of this 40-kva alternator are shown in Figure 3. It will be noted that the length is 19 inches and the diameter of the body is nine inches. The 19-inch dimension includes the air connector shown on the right, but does not include the shaft extension and pilot fit on the left. These

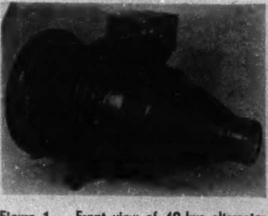

Figure 1. Front view of 40-kva alternator showing air connector and mounting band

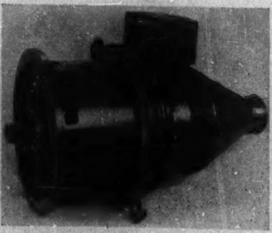

Figure 2. Rear view of 40-kva aircraft alternator showing bracket and shaft extension

Paper 44-190, recommended by the AIEE committee on air transportation for presentation at the AIEE Los Angeles technical meeting, Los Angeles, Calif., August 29–September 1, 1944. Manuscript submitted June 22, 1944; made available for printing July 11, 1944.

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The authors acknowledge the assistance of J. C. Cunningham, L. A. Kilgore, and E. C. Whitney, of the East Pittsburgh Works, and J. D. Miner of the Lima Works, of Westinghouse Electric and Manufacturing Company.

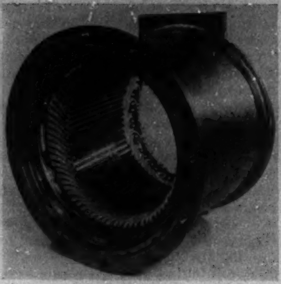


Figure 4. Wound stator before impregnation

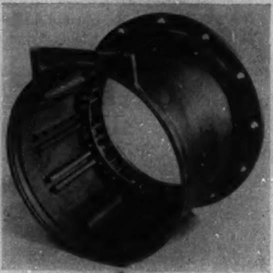


Figure 5. Magnesium-alloy frame of alternator showing internal ribs

Figure 6. A-c stator coils

outline dimensions as a whole conform to specifications of the United States Army Air Forces. The alternator is designed to bolt to the constant-speed drive to make, in effect, a single unit. Pilot fit and splined shaft of the alternator engage corresponding parts of the drive.

Figure 3 also shows an adjustable two-piece mounting ring. This ring with its four projecting ears serves to support the alternator end of the combined unit in the airplane framework. Adjustability of the ring over a range of four inches axially facilitates installation. The body of the alternator, over which the ring is adjustable, is machined to a smooth, accurate, cylindrical contour, so that the ring may be secured firmly in any position within the limits of adjustment.

Coupling to the constant-speed drive is through a replaceable 24-tooth driving spline. Some radial freedom, to allow for a small amount of misalignment between the alternator and constant-speed unit, is provided by use of a stub shaft, the opposite end of which is spline-fitted to the inside of the main shaft of the alternator. A shear section is provided in the stub shaft for protection of the constant-speed drive in case of combat damage or mechanical failure of the alternator. Removal and replacement of the stub shaft does not require dismantling the alternator. If broken, the stub shaft can be driven out easily by inserting a rod in the hollow shaft at the air-connector end of the machine.

Method of Excitation

Excitation is furnished by an integral d-c exciter of conventional aircraft-generator construction, an arrangement chosen after a careful study of all possible methods of excitation. This arrangement is lightest in weight, is less complicated, requires the smallest regulator, and is more stable than any other method considered. On the *XB*-19 airplane, excitation power was obtained from the a-c output, which was rectified and controlled electronically.[2] The latter arrangement inherently means loss of excitation in the event of a fault, and the alternator will not provide enough current to burn clear many types of faults which might occur. This arrangement had the further disadvantages of added weight, and also of appreciable warm-up time for the electronic tubes. Use of an inductor alternator to supply excitation power was considered because of the advantage of eliminating brushes. However, an inductor alternator is heavier than a d-c machine and requires the use of a rectifier, which would need to be mounted in or on the alternator in order to make the latter interchangeable with another unit which might use a d-c exciter. Such an arrangement would be awkward and heavy.

Excitation might have been furnished from batteries or from the d-c power

Table I. Comparative Weights of Electric Systems

	System		
Components	28-Volt D-C Self-Excited Generator	120-Volt D-C Self-Excited Generator	208/120-Volt 400-Cycle 3-Phase Built-In Exciter
1. Generator weight, one unit	150 lb	120 lb	80 lb
2. Voltage regulator, one unit	6 lb	6 lb	9 lb
3. Main generator breaker, or switch	6 lb	6 lb	5 lb
4. Drive shaft from engine	15 lb	15 lb	15 lb
5. Constant - speed drive			90 lb
6. Total, items 1 to 5	177 lb	147 lb	199 lb
7. Total weight of four units	708 lb	588 lb	796 lb
8. Batteries, total weight	55 lb	55 lb	55 lb
9. Two 28-volt 6-kw d-c sources of power	0 lb	125 lb	100 lb
10. Two 12-kva sources of single-phase 400-cycle power	300 lb	280 lb	0 lb
11. Four sets of automatic synchronizing control			40 lb
12. Weight of main power wiring	420 lb	55 lb	65 lb
13. Total weight excluding utilization equipment	1483 lb	1103 lb	1056 lb

Airplane is a long-range bomber, weighing 150,000 pounds; continuous electric load of 60 kw. Wing spread, 170 feet; length of fuselage, 110 feet; 45 feet to outboard engine; 28 feet to inboard engine. Generator specification:

30 kw continuous, 45 kw for five minutes
Speed range, 3,000–9,000 rpm
Not engine-mounted
Blast-cooled
Altitude, 0 to 50,000 feet

This table was developed at a round-table conference conducted by Major W. A. Barden, United States Army Air Forces, equipment laboratory, Wright Field. Members of conference were: George W. Sherman, Wright Field; L. G. Levoy, R. H. Kaufmann, General Electric Company; J. C. Cunningham, D. E. Fritz, C. G. Veinott, Westinghouse Electric and Manufacturing Company.

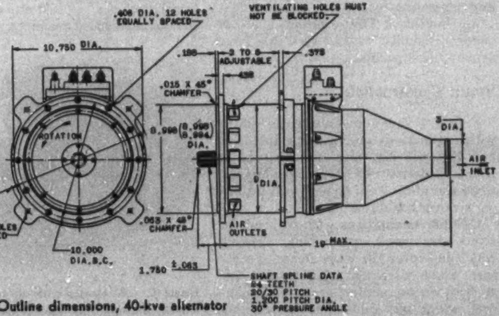


Figure 3. Outline dimensions, 40-kva alternator

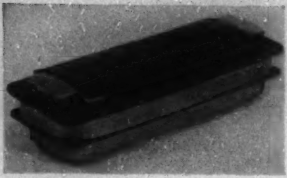


Figure 7. Complete pole and field coil of alternator

system of the airplane. Such an arrangement has three principle disadvantages:

1. A fault in the d-c system would cause failure of the a-c system.
2. A larger and heavier voltage regulator would be required, involving a major development, as such a regulator is not now available.
3. There would be an excessive waste of power in the regulator.

Brush life at high altitude—the principal objection to the use of the d-c exciter—was no longer considered the problem that it once might have been, since a successful altitude treatment for brushes is now available.[4] Based on flight experience with d-c generators and altitude-chamber tests on the alternator, the expected brush life at 35,000 feet is approximately 500 hours.

Electrical Design Considerations

Fundamental principles employed in the design of aircraft alternators and d-c generators are the same as those used in the design of standard industrial machines. The elements of an aircraft electric machine resemble those of an industrial machine of far greater physical size. However, in the design of aircraft electric machines, extreme effort is put forth to obtain a given rating at the lowest possible weight; thus, many refinements are required in the electrical and mechanical design which are unwarranted in standard machines.

Electric conductors of aircraft alternators, as well as those of engine-mounted d-c generators, are operated at current densities far in excess of standard machines; 10,000 amperes per square inch is not uncommon[4]—a figure four times as high as used in continuous-rated industrial machines. Magnetic circuits of this 400-cycle aircraft alternator are worked at about the same densities as conventional 60-cycle machines. Because of the much higher frequency, however, the watts loss per pound of magnetic material is from 15 to 20 times as high as in a standard 60-cycle machine. Although the copper and iron are worked harder in aircraft than in industrial machines, the efficiencies of the former are as high or higher for comparable ratings. This is possible because of greater refinements in design, higher operating speeds, and better ventilation of aircraft machines.

Insulation of the windings of aircraft alternators must be suitable for the temperatures encountered. Because indefinite life is not expected and could not be obtained without a great increase in weight, class *A* and class *B* insulating materials usually are operated at higher temperatures in aircraft than for industrial service. Thus, for the life expected of an aircraft alternator, class-*A* insulation can be operated at 130 degrees centigrade and class-*B* insulation can be operated at 150 degrees centigrade.[4] Each type of insulation has been used in this alternator only where its maximum permissible temperature will not be exceeded.

D-c generators for aircraft use are rated at 28.5 to 30 volts, whereas the 208-volt alternator described in this paper is operated at 120 volts to ground. Because of the higher dielectric stresses, the a-c winding is given a potential test to ground of 1,500 volts (rms), and a potential test between phases of 500 volts; alternator field and exciter armature are tested at 500 volts to ground.

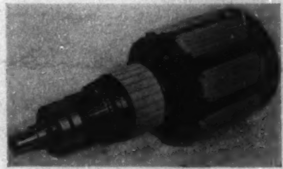


Figure 8. Complete rotor showing alternator field, slip rings, exciter armature, and commutator

As in the d-c system, greatest reliability is obtained with parallel operation,[2] and the alternators are designed accordingly. As mentioned before, a damper winding is placed in the pole faces to provide more stable operation of the alternators under any operating condition which might cause hunting. This winding, made of copper, is similar to that used in a large synchronous machine.

Stator Construction

Figure 4 shows the alternator stator with laminations and winding in place, and Figure 5 illustrates the lightweight frame. Internal ribs support the laminations and allow axial passage of ventilating air over the stator core.

Circular laminations without studs or rivets are used, permitting skewing which improves the wave form. To ensure exact alignment, laminations are stacked on an accurately ground mandrel, which has guide bars for aligning the punchings and obtaining the correct skew. While still on the mandrel, the punchings are compressed axially, and a slot is machined in the outer periphery parallel to the axis of the stator. A corresponding slot is machined in one of the ribs of the magnesium-alloy frame, and a rectangular key fits into both slots, thereby locking the punchings.

Stator laminations are held in compression between a shoulder in the frame and a spacer ring held in place by means of six axial through bolts. Openings are provided in the ring and shoulder so that ventilating air can enter the axial ducts between laminations and frame.

A-c stator coils are formed of double-glass-covered rectangular copper wire. Two of these coils are shown in Figure 6. Accurate forming assures that the coils will not touch each other at the ends and that they will have the strength necessary to withstand short-circuit stresses. Class-*B* insulation is utilized in the a-c winding. Paper-and-mica slot cells are used, the paper backing merely providing strength during assembly. Glass sleeving is slipped over the exposed portion of each stator coil. Coils are anchored in slots by means of trapezoidal wedges. After winding, the a-c stator is impregnated with an alkyd resin varnish to resist the moisture, dirt, and fungus growths which an aircraft alternator may encounter.

Rotor Construction

Two obvious methods for constructing the rotating field of alternators such as described in this paper are one-piece laminations and separable poles. With one-piece laminations, the poles and pole faces are integral with the yoke; this type of construction has been used with success in many small machines and in a few large four-pole industrial machines. Since the yoke and poles are continuous, a good magnetic circuit is obtained through the rotating field. However, the one-piece rotor punching has the disadvantage of requiring a random field winding, which is satisfactory in small low-speed machines, but seldom if ever, is used in large high-speed machines. This con-

Figure 9. Alternator with air connector removed, showing front bracket and rocker ring

struction was not adopted in the machine described in this paper because of low mechanical strength and poor space factor inherent in a random winding.

Figure 7 illustrates the separable-pole construction used in this machine. Pole laminations have a rectangular hole in the body, by means of which they are assembled onto a snug-fitting rectangular bar of iron having good magnetic properties. These laminations are clamped together by means of the damper bars, a steel through rivet, and aluminum-alloy pieces which also serve to support the coil ends. Holes are now drilled and tapped through the punchings into the rectangular bar previously described. This construction permits the field coils to be wound directly on the poles.

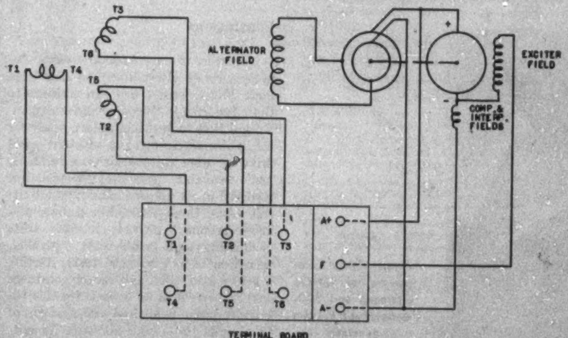
A heavy layer of fish paper, glass, and mica insulates each coil from the body of the pole. Each coil is wound in two sections of thin copper strap insulated between turns with glass tape. Micarta washers insulate the upper section of the coil from the pole and the coil sections from each other.

After the coils have been wound, they are bolted to a cylindrical yoke and then connected between poles at this time in order to avoid disturbing them later. Wedges are now inserted between adjacent coils in both upper and lower sections. After connecting the coils and inserting wedges, the rotor is thoroughly vacuum-impregnated with an alkyd resin varnish and then baked. Thus, a solid rotor winding is obtained, free of air pockets and impervious to moisture.

Alternator field, exciter armature, commutator, and slip rings form an integral unit. The exciter armature is pressed onto the long shaft, flanged at one end and bolted to the yoke of the alternator field. A shorter flanged shaft is bolted in a similar position to the opposite end of the yoke. Thus, the complete rotor utilizes only two bearings. This construction greatly simplifies assembly of the alternator and prevents any possible internal misalignment.

Figure 10. Internal wiring diagram and terminal arrangement of 40-kva alternator, including exciter

When rotation is clockwise (at commutator end), phase sequence is *T*1, *T*2, *T*3



Exciter Construction

The front bracket of the alternator supports the stator of the exciter, as well as the rotor, bearing, and brush assemblies. This construction is illustrated in Figure 9, a view of the alternator with the air connector removed, showing the arrangement of the exciter in the bracket. Exciter brushes and slip-ring brushes are all carried on the rocker ring, which is mounted in the front bracket. By reference to the schematic wiring diagram, Figure 10, it can be seen that the slip-ring brushes are connected directly to the output of the exciter, thus requiring minimum wiring between the exciter and alternator field. To provide maximum excitation for the alternator when required, a compensated exciter with interpoles is used. Two collector rings are mounted at the front end of the rotor, adjacent to the exciter commutator. The rings are shrunk over an insulated steel bushing, which later is pressed onto the exciter shaft.

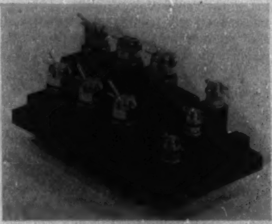


Figure 11. Terminal board and studs, showing stepped arrangement

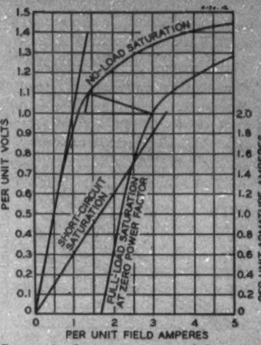


Figure 12. Saturation curves and short-circuit characteristic of 40-kva alternator at 6,000 rpm

Mechanical Design Considerations

Aircraft generators must operate with minimum attention and trouble. No part of a generator is more important to reliable operation than the bearings, and careful consideration must be given to their selection. Sealed or shielded antifriction bearings offer excellent possibilities for aircraft generators, because they require no attention. Bearings are lubricated at the factory with grease of proper grade and quality to assure long life. When the lubrication is properly applied, a bearing life of several hundred hours may be expected. Because it is in the path of the incoming air, the front bearing is unlikely to become overheated. However, special precautions have been taken to make certain that the rear bearing is adequately cooled. The exciter shaft is hollow, and the front end is open to the incoming air, while at the rear end four holes are provided in the shaft flange. Cooling air passes through the rotor, and then over the rear bearing, thereby preventing excessive temperature rise. This ventilating path serves a second purpose, that of helping to cool the rotor yoke.

To obtain minimum weight, both the front and rear brackets are cast of magnesium alloy. The rear bracket has six arms, channel-shaped for increased strength. The cone-shaped front bracket has four ribs which serve to support the the exciter stator and rocker ring, as well as the front bearing.

Engine-mounted d-c generators are subjected to more severe linear and torsional vibrations than encountered in any other type of service. History of the engine-mounted d-c aircraft generator records a great amount of progress in the solution of the problems involved. Be-

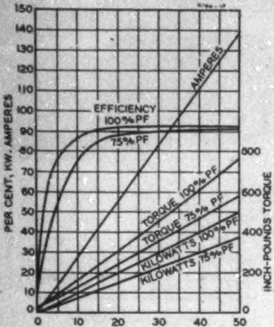


Figure 13. Efficiency, torque, and output curves of 40-kva alternator at 6,000 rpm, from dynamometer tests

cause the alternator of this paper is not engine-mounted, the problem of linear vibrations and accelerations is less severe; however, the machine is subjected to all the vibrations and accelerations occurring in the nacelle structure. Torsional vibration presents a severe problem of undetermined magnitude, because torsional impulses may be transmitted through as well as developed in the constant-speed drive. If the generator is mounted on an auxiliary engine, severe vibrations, both linear and torsional, may be encountered.

Ventilation and Cooling

Ventilation is unquestionably one of the principal factors influencing the weight of any aircraft generator. Without blast-tube cooling,[6] the extremely high outputs per pound of modern aircraft generators would not have been possible. Aircraft-generator builders have been hampered by lack of accurate information on the pressure–volume characteristics of practicable aircraft blast-tube systems. Shall the generator designer use restricted ducts in his machine, or shall he make them as wide open as possible? Few problems are more difficult to solve than the precise answering of this question. In general, the generator designer knows that, with blast-tube cooling, the less air his machine uses, the more total pressure head is available at the generator; and, conversely, that a wide-open generator which uses more air results in less available total head across the generator. Total available head and volume of air could be determined by the generator designer, as pointed out in a recent AIEE paper,[5] from a pressure–volume curve of the blast-tube installation, if the latter were available.

Some general considerations on the qualitative differences in the cooling problem of wide-open and restricted generators will assist in an understanding of the ventilation problem. The significant factor is total temperature rise of the winding hot spot above ambient, which is the sum of two temperature rises:

1. Rise of cooling air passing through the machine.
2. Rise of hot spot above the cooling air.

A wide-open generator passes more air; hence, rise of cooling air is invariably less. But, in the wide-open generator, the dissipating area of the cooling ducts is less, and air velocities in the ducts tend to be lower; the net effect is to cause a higher rise of hot spot above the cooling air. Since total temperature rise is the important factor, it can be seen that the minimum sum of the two factors occurs when the generator is neither too wide open nor too restricted. A complete answer to the ventilation problem requires an analytical or empirical evaluation of the hot-spot rise for different degrees of restriction, taking into account the pressure–volume characteristic of the blast tube.

For the 40-kva alternator discussed in this paper, a total head of at least six inches must be maintained between the inlet and outlet. If the alternator is installed in a large duct which supplies air to the main engine, an exhaust shroud around the outlet ducts of the alternator has to be provided, because the entire alternator is surrounded by air at a relatively high pressure; this shroud must discharge to a region of lower ambient air pressure.

Air ducts have been provided between the outside diameter of the stator punchings and the frame. Air which passes through these ducts also passes directly over the end windings, picking up heat from them as well as from the stator laminations, and from the frame which is a good conductor of heat. Cooling air also flows axially between the field coils of the rotor. The front bearing is cooled by the main blast of air as it enters the alternator, whereas the rear bearing is cooled by air which flows through the hollow shaft, as explained previously.

No effort has been spared to provide the best cooling obtainable with the specified total head of six inches, and the temperature rise has been held down to permissible limits. But it must not be overlooked that, however effective the ventilating system may be, for any given pressure, better results will be obtained from the alternator if higher pressure or more cooling air is made available. By better results are meant lower temperatures of the alternator, with consequent longer life of insulation and bearings, and ability to carry higher overloads of short-time duration.

Performance

Saturation curves of the alternator are given in Figure 12. These curves include: a no-load saturation, a zero-power-factor saturation, and a three-phase short-circuit saturation. Dynamometer tests are recorded in Figure 13, which shows efficiency, torque, kilowatts output, and amperes output, plotted against kilovolt-ampere output. Efficiencies reported here include losses in the exciter, voltage regulator, and alternator itself; they were computed from direct measurements of input and output.

Conclusions

A number of experimental 40-kva 400-cycle aircraft alternators has now been built, and extensive tests, in addition to those reported in the paper, have demonstrated that such machines are ready for use in aircraft as soon as a constant-speed drive or other suitable driving means is made available. (Some of the tests not reported in the paper include: short-circuit tests, three-phase, line-to-line, and line-to-ground; altitude-chamber tests of heating and brush wear; parallel-operation tests; overspeed tests; starting a heavy load, such as a motor generator set.) A weight of 85 pounds for this 40-kva alternator, compared with a weight of 47 pounds for a nine-kilowatt d-c air-

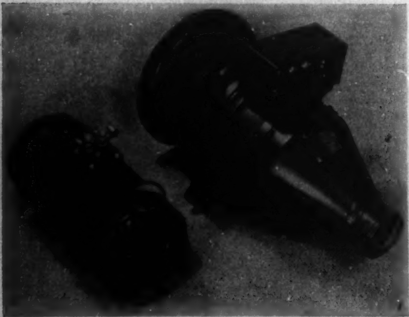


Figure 14. Comparison of 40-kva alternator and 300-ampere type *R*-1 d-c generator

Aircraft Electrical Horizons

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AN ENGINEER needs a goal. In this respect he is no different from the follower of any other profession, art, or trade. Having a goal, every effort and project becomes a step in the final achievement. It is the purpose of this paper to suggest some goals which are on the horizon today. When these goals are reached, it is quite likely that new horizons can be seen. However, let tomorrow take care of itself.

A goal is not only an incentive, it is a pathway. Seeing the need for a device is more than half the act of inventing that device. It can well be that presentation of some of aviation's needs will stir the inventive genius of some engineer who will then create a solution. It is by such steps that we will extend the horizon.

The importance of the electric system looms large today. In former installations the hydraulic and electric systems were not essential to flight. Soon this will no longer be true. Already designers of automatic control apparatus are placing faith in the aircraft electric system. If an inverter delivering a-c power to a turbo supercharger regulator should fail, engine manifold pressure would no longer be controlled; it happens that the engine air-induction system is unstable and a dangerous operating condition can be created in a few seconds. The source of a-c power must not fail. This is typical of situations that emphasize reliability of the electric system.

Electric-System Protection

Hand in hand with proper reliability of separate electrical components is the necessity for automatic protection of the system from the power sources through feeders and circuits to the individual items of equipment. The state of the latter art is not at all satisfactory; almost without exception protective devices have one or more of the following faults:

1. They do not duplicate the thermal ability of the apparatus being protected.
2. They are too heavy.
3. They have serious errors due to ambient temperature.

For example, circuit breakers are used to protect the cable (not the equipment) of a circuit. A given rating of circuit breaker should permit the cable to carry every ampere that it can safely carry. In fact, if the perfect circuit breaker were developed, it would be designated by an American wire gauge number.

Generators and Motors

The electrical industry can be proud of the progress that has been made in aviation accessories during the last few years. The output from a cubic inch of space and material has been doubled, quadrupled, and doubled again. The engine-driven generator is the best example of this progress. Almost since the beginning of modern aviation the generator designer has been confined to a space of 6 inches diameter and 14 inches length, a volume of 400 cubic inches. Until 1939 this space produced 750 watts, less than 2 watts per cubic inch. Today, nearly 30 watts per cubic inch are developed in that space. The output constant for this type of machine is approaching 20,000, approximately three times the conventional value for the equivalent rating. This constant is derived from the relation

$$c=\frac{D^2LN}{kw}$$

where D and L are dimensions of the active material in the armature, N is speed in revolutions per minute, and kw is output in kilowatts.

At the same time good efficiency has been maintained. At first glance this is surprising since the power loss per pound of material is much higher than in industrial practice. But the number of pounds is reduced until efficiencies are tolerable. This factor is important to aviation; low efficiency means a higher power consumption and this in turn requires the use of fuel at a greater rate. Gasoline weighs six pounds per gallon and an engine requires nearly 0.5 pound per horsepower hour. A generator having an efficiency of 70 per cent will require one pound of fuel per kilowatt-hour.

However, industry has been too prone to modify an existing article to meet a given aviation need. This may be justified in cases of extreme haste or where cost of development must be avoided. But it must be remembered at all times that in aviation a pound saved may be worth its weight in gold.

Similar progress has been made in d-c motors. Just as the specific weight of generators has been reduced to four pounds per kilowatt, so has the weight of motors been reduced to four pounds per horsepower at comparable speeds. Still lower weights are on the horizon and one of the steps to obtain them is higher speed. This will require further development in bearings, lubrication, and commutator design.

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craft generator, indicates that electrical outputs of generators still can be expected to increase faster than the weights. Experience gained in the building and testing of this alternator can be used to develop better and lighter machines, or machines of still higher rating; the limit of ratings of aircraft generators is not even in sight!

Perhaps, in the future, direct-connected alternators can be used to maintain aircraft engines in synchronism. For test purposes, these 40-kva alternators were direct-connected to 450-horsepower aircraft engines; after the alternators were synchronized manually, it was found that they would hold the 450-horsepower engines in exact synchronism over a wide range of throttle settings, even while carrying electric loads at the same time.

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Communication Equipment

Much that has been accomplished in the field of radio cannot be unveiled at this time. The importance of these developments can be realized, however, when it is noted that there are as many as 12 complete radio equipments in a large bomber. Each of these is designed for a special task and only a few of them are for radio communication as we know it. These new devices extend the horizons again for each of them presents an interesting peacetime application. And again, the aviation engineer is hoping that the radio engineer has a weight horizon. Much of this new apparatus is unnecessarily large and heavy. This handicap has been overlooked in the necessary haste to obtain useful devices for the war effort. But thoughtful study will eliminate many pounds of equipment weight and much aerodynamic drag from external antennas. Antennas appear to weigh only ounces, but drag increases the horsepower required from the engine and this in turn causes the consumption of many extra pounds of fuel.

Aircraft Lighting

The lighting of aircraft has never been perfect. While much has been done, far more remains undone. The position lights are adequate only in fair weather. A good landing light for fast fighter types is needed. Really satisfactory lighting for the instrument board and the pilot's compartment are yet to be produced. It is unlikely that any passenger is happy with the reading lamps in the modern transport. Failure of a landing-light lamp during testing prior to take-off is cause for a delay since the pilot is compelled to return to the hangar for a lamp replacement. A reserve is needed.

The problem of lighting instruments and the pilot's compartment is the most severe. Many minutes are required for the human eye to accommodate itself to darkness. Yet this dark adaptation can be lost in a second if a bright light is turned on in the cockpit either by design or accident. The lighting of instruments and important controls must be such that they are easily visible without impairing night vision.

Inverter Power Supplies

The development of inverters, direct current to alternating current, has made it possible to utilize alternating current for many small devices, particularly controls and instruments. Both of these uses are worthy of comment.

The inverter has usually been made as a motor generator and the resultant weight and low efficiency are a severe penalty. The penalty is the more noticeable when it is remembered that electric energy is put into the machine and electric energy is taken out. Perhaps, over the horizon is a way of avoiding the change to and from mechanical energy. The vibrator type of inverter has been successfully used at very low power and at frequencies ranging from 60 to 400 cycles per second. At the higher frequency this type will deliver very little power, although this frequency is most suitable for aircraft.

A-c power is well suited to many positioning and control devices since the controlling voltage can be amplified readily. The principle of the self-synchronous motor has been used to advantage in instruments which deliver a remote indication.

Relays and Switching Devices

Other items which receive too little attention are switching devices, and yet all the reliability of the system depends on them. Electric apparatus in aircraft all too often is the cause of an unsatisfactory report, because a relay failed to close, switch contacts welded, moving parts corroded, or any one of several other faults. Practically all of these can be traced to dirt, moisture, or salt air. And all of these can be avoided by hermetically sealing the switch, relay, or magnetic contactor. At the same time this change in design will remove all restrictions on altitude ratings, will make the device explosion proof, may permit an increase in rating, will increase reliability, and it is believed that this can be done at no increase in weight. In fact, weight should decrease if the rating can increase. This type of construction may be a basic requirement for switching devices in the near future.

Load and Equipment Considerations

As many as 150 electric devices have been used in several types of aircraft. This indicates the reason why the electrical load and the installed generator capacity per airplane have increased and are still increasing at a phenomenal rate. As an approximate rule it can be said that the generating capacity in kilowatts should equal the gross weight of the airplane in tons. And in terms of engine power, two per cent of the rated power or three per cent of the cruising power should be reserved for the generator.

In producing these devices the electrical engineer has gone far toward limiting conditions in his use of materials. For example, only the best of magnetic materials are used and these are worked to the limit; the best of materials are fully justified because they save weight. Weight saved is invaluable and the cost of materials is small. An airplane in the transport or bomber class costs $5.00 to $7.00 per pound. Manufacturing costs should be judged by that standard. The aviation engineer hopes that a good magnetic material having the specific gravity of magnesium is just over the horizon.

The current density in conductors has been pushed to new values. Ten thousand amperes per square inch and 125 circular mils per ampere are often used in continuous-duty apparatus. This requires careful engineering to provide adequate cooling, but the net gain is a considerable reduction in weight. For intermittent-duty applications theoretical current densities of 40,000 amperes per square inch have been used. Better conductors will permit higher densities, or still better cooling will do the same. At one time the use of silver instead of copper was proposed, but this was done not as a matter of conductivity but one of supply.

The current density in carbon brushes is limited only by temperature. Where densities of 40–60 amperes per square inch are conventional, values of 200 amperes per square inch at rated output are common in aircraft generators. With adequate cooling, densities of 600 amperes per square inch are on the horizon.

Power-Distribution Systems

As airplanes increase in size and electrical load, a decision about higher voltages is forced on the aviation industry. The standard for military aircraft is the 24–30-volt d-c system. This standard is likely to include the postwar transport and to supersede the present 12–15-volt d-c system. However, the 24–30-volt system is inadequate when 25–100 kilowatts are being considered and when airplanes with wing spans of 150 feet and more are discussed. A conductor to carry 100 amperes a distance of 100 feet with a voltage loss of 0.9 volt (three per cent of 30) is altogether too heavy.

Since it is a miniature model of a public utility, the electric system of a multiengine airplane presents many similar problems. An increase in system voltage has the same advantages to the aircraft as to the utility company. It is advantageous because it reduces the weight of wiring faster than voltage increases. Theoretically, the wiring weight should be inversely proportional to the square of the operating voltage. This reduction cannot be realized because the insulation becomes a greater proportion of the total conductor weight as size is decreased and because it is not practical to use a wire size of less than number 20 American wire gauge. The latter limitation is due to the structural strength of the conductor.

Two higher-voltage systems are already on this side of the horizon. One is direct current and one is alternating current. The d-c system has much to recommend it; it is similar to the 24–30-volt system already in use. Problems of commutation and switching which were forecast as trouble makers have not been difficult on actual encounter. The progress already made in reducing weight of 24–30-volt equipment applies equally well to similar items of higher voltage. One of the largest users of electric power is radio, and by a judicious choice of voltages and care in the design of the radio equipment the need for inverters, dynamotors, and power packs can be avoided.

A big advantage of the d-c system is that it is possible to use a battery as a reservoir of energy and as a means of absorbing surges. The ampere-hour capacity of the cells should equal about half the current rating of the generator and there should be as many batteries as there are generators if the battery is to fill its place as a power reservoir properly. The proposed battery would consist of two 48-cell units if the lead-acid type were used. Unfortunately, battery weight for constant watt-hour capacity increases with increasing rated voltage.

The choice of system voltage is a most important problem. At least a portion of the power should be available at the familiar 110–120-volt level to use or adapt apparatus already developed for industrial use. At the same time higher-power loads such as large motors can advantageously use a still higher voltage. This suggests the familiar three-wire system at 230/115 volts. In metal aircraft the neutral wire can be omitted by using a structural ground.

A-C, D-C, and Hydraulic Comparisons

The a-c system has had little practical experience to support claims advanced for it in aviation engineering, although the potential advantages are similar to those which caused its adoption for industrial power. It has several disadvantages to overcome. Paralleling of generators is essential and power usually must be taken from the main engines. Since the latter will seldom operate in synchronism, a speed- and torque-conversion drive is necessary. The drive and generator will undoubtedly weigh more than a d-c generator of equal rating.

Power factor increases the current and therefore the conductor weight. Using a three-phase system, the operating voltage must be at least 40 per cent higher than that of the equivalent d-c system to result in the same weight of conductor. The equivalent of the d-c system described above would be the 208–120-volt four-wire three-phase system and the weight of conductors for equal power would be higher.

Offsetting these disadvantages are the lower weights of a-c apparatus, simpler construction, reduced arcing hazard, improved efficiency, reduced maintenance, flexibility of voltage, and possibly greater reliability and longer life.

Electric apparatus has many advantages over hydraulic and pneumatic equivalents that almost force use of the former for many of the aircraft accessory tasks. Some of these advantages are:

1. Higher efficiency.
2. Lower cost.
3. Ease of installation.
4. Simpler maintenance.

All of these are debatable with a proponent of any other method, but they are the advantages which the electrical engineer must exploit.

On the debit side, the electrical engineer has not delivered enough

1. Reliability.
2. Life.
3. Reduction in weight.

To provide adequate reliability the device must never fail in service and should require no attention for 1,000 hours of airplane operation as a minimum. With simple overhaul, the useful life should be at least five of these 1,000-hour periods. As for weight, a part is always too heavy. In addition, the electrical engineer must fit his device into the needs and requirements of the aviation industry and the operations of both airline and military aircraft.

In 1930 the operating cost for airline transports was approximately 60 cents per ton mile. In 1940 it was 30 cents. Perhaps in 1950 it will be 15 cents. Anything which makes that cost a reality will be welcome. By 1950 it may be possible to operate engines at a fuel consumption rate of 0.37 pound per horsepower-hour instead of the present rate of 0.45–0.46. This economy of operation will require automatic controls, and electric devices are ideal for such applications.

An airplane earns dividends only when it flies. The airlines will want a utilization factor of 60 per cent. This means that the airplane will be in service six hours out of ten. This leaves no time for needless maintenance of awkward electrical accessories.

Other Tasks for Electric Devices

The foregoing remarks have presented attainable goals and requirements. Some other big problems are also on the horizon. For example, one accessory which the electrical industry has never produced is the aircraft wheel brake. For the most part this task is performed hydraulically; it has also been done with pneumatic power. To present the requirements of this problem the assistance of the Goodyear Tire and Rubber Company was sought; R. J. Lammertse and C. E. Ricker, engineers of the wheel and brake division, kindly furnished information on which the following is based. Emphasis has been placed on the particular phases of the problems which are most likely to be difficult to overcome in an electrically actuated brake. The requirements set forth below are divided into two groups, the first dealing with force and torque, and the second dealing with operating characteristics.

1. Force and torque requirements. The brake must produce enough tangential force at the mean friction radius to give a torque of 0.55 WR where W is the weight of the airplane divided by the number of brakes per airplane, and R is the loaded radius of the tire. Usually, the mean friction radius is between 0.3 and 0.5 of the loaded radius of the tire. The normal force required between the friction surfaces will vary inversely with the coefficients of friction of the pairs of material used. For the bimetallic friction pairs used in multiple disk brakes, the coefficient ranges from 0.1 to 0.2, and for molded lining material against chrome plated steel or cast iron as used in drum and single disk brakes, the coefficient ranges from 0.2 to 0.3. These coefficient ranges are determined through experience to be found in the friction material pairs most satisfactory from a wear and durability standpoint. Of course, the force required will also vary inversely with the number of friction surfaces engaging under the same force.

2. One of the major difficulties of an electric control system is to obtain proper load feel comparable to manual operation. This would include all of the following points:

(*a*). Brake torque should be proportional to pedal pressure. This should be true regardless of whether the brake operation is going on or off.

(*b*). Brake operation should go from full-on to full-off position or vice versa with no appreciable time lag.

(*c*). Brake operation must be capable of small changes of torque in either direction without time lag and without returning to brake-off position.

(*d*). The brake must be capable of being held at constant torque during the period of stop.

(*e*). Some means of parking the brake for long periods of time must be provided.

In the design of an electric brake-operating system, it must be remembered that the weight and the space used should be reasonably close to the weight and space requirements of an equivalent hydraulic system. This, of course, applies to the entire system, from the pilot's control down to the brake itself.

Another big problem which is rolling up over the horizon is the prerotation of wheels prior to landing. This is desired to avoid unusual wear of tires upon impact with hard-surfaced runways. It will not require much power, as ample time can be allowed to bring the wheel up to speed. If the electrical engineer can solve the brake problem in less space and weight than is now required for the hydraulic system, there may be space enough for a small electric motor to deliver prerotation of the wheels.

The problems of controlling an airplane, its engines, and some of its accessories increase with aircraft size and again with altitude. For example, it is possible now to build airplanes so large that human effort cannot control them. Power boost is needed. It can be hydraulic or it can be electrical.

At high altitudes pressurized cabins will be a necessity. To operate cables or push rods through the walls of a pressurized cabin requires meticulous care in design, installation, and maintenance. Rotational couplings at the cabin walls are only slightly better. Electric equipment is ideally suited for the solution of this problem.

Automatic controls for flight, engines, engine accessories, heating systems, ventilating systems, and other aircraft luxuries will be a big advantage to the airplane crew. Again electric apparatus is ideally suited for these applications.

Electric Propulsion of Aircraft

The biggest task that is just peeping over the horizon is an electric drive for the airplane. In other words, this means an electric coupling of generator and motor between the power plant and propellers.

Assuming that weight can be kept within reasonable limits, there are a number of advantages favorable to the use of electric-propulsion equipment on multiengine airplanes.

One of the largest single elements of drag to be overcome in propelling the conventional airplane through the air is due to nacelle and cooling losses, with engines in the leading edges of the wings or in the fuselage nose; this element of drag may amount to from 20 per cent to 40 per cent of the total horsepower requirement of the engines. Because of the relatively small size of an electric motor compared with engines, propellers may be placed where the designer wishes, submerging the motors entirely within the wing or other structural element without recourse to drag-inducing nacelles. In some instances, the weight saving due to the reduced fuel load when nacelles are eliminated may be sufficient to offset the increase in power-plant weight due to the addition of the electric transmission.

With electric drive, the prime movers and generators are located in the fuselage instead of in the wings. In land planes, it may sometimes be necessary to employ a somewhat larger fuselage than normal to accommodate the engines; in the case of sea planes; there is always sufficient space in the hull near or below the water line, a preferred location. With the engines in locations readily accessible to the crew, flight operations may be carried out with standby engines disconnected and all propeller motors running in parallel from a common power bus; and when necessary, simple repairs may be effected on standby units in flight.

Parallel operation of motors will eliminate the entire synchronization problem; and the removal of heavy-overhung engine mounts from wings should permit desirable structural redesign with over-all improvements in structure weights. The concentration of the major mass of the prime mover in the fuselage near the center of gravity of the airplane increases maneuverability and decreases the required control-surface areas; it further provides a convenient recoil-absorbing mass to take the recoil from heavy machine guns and cannon in military aircraft, without the necessity of shooting through a propeller.

Separation of engines from wing structures and the possibility of employing effective shock-absorbing engine mounts may also lead to structural weight savings. Breaking up the motor drives into a large number of lower-power units will also be advantageous in this case, and will permit using small lightweight two- and three-blade propellers with the higher efficiencies inherent in such propellers when over-all design permits, instead of using lower-efficiency three- and four-blade propellers of large power ratings.

A further advantage is the possibility of employing pusher-type propellers instead of tractors, since motors may be sunk in the wings. The increased lift and reduced drag over wing sections in the vicinity of propellers are pronounced at high speeds when propellers are placed behind the wing rather than in front of the wing, thereby reducing turbulence due to the propeller slip stream over the wing.

On the other hand, an electric drive will result in increased power-plant weight, lower efficiency between fuel tank and propeller shaft, increased cost of power plant, and with gasoline-engine prime movers, probably no reduction in maintenance. Further, the installation of engines and alternators in the fuselage of a bomber or cargo airplane may result in disadvantageous bomb or cargo location.

With the above features established, it appears that the most advantageous system would require the use of high-speed high-frequency induction motors and corresponding synchronous generators. Estimates have been made based upon three-phase induction motors to operate in the 400–500-cycle range at a transmission voltage of the order of 1,000 volts, and with turbo-type alternators to furnish power for the motors.

Preliminary analyses have been made and it is believed to be practical to produce an electric coupling, alternator, motor, and controls at the following weights:

Horsepower	Pounds	Pounds Per Horsepower
1,000	750– 850	0.85
2,000	1,200–1,300	0.65
5,000	2,300–2,700	0.54

At each of these ratings the alternator and motor weights will be divided about in the ratio of 60 and 40 if both can operate at approximately the same speed.

The probable advent of high-speed internal-combustion turbines will greatly improve the case for electric drives. The weight and the efficiency loss of the coupling are handicaps and there must be two gains to compensate for them:

1. Compensating reductions in fixed weight must be made in the power plant, propellers, structure, and accessories.
2. The over-all efficiency of the airplane must be improved by eliminating aerodynamic drag to compensate for the lowered efficiency of the coupling.

Storage of Electric Energy

The storage battery is another problem. Perhaps there is no answer, but even with aircraft designs of lead-acid storage batteries, the storage of a kilowatt-hour of energy costs approximately 100 pounds. The same available energy is stored in 0.25 pound of gasoline. Both represent chemical storage of energy.

The electric motor which converts electric energy to mechanical energy weighs less at equal ratings than does the internal-combustion engine which converts gasoline to mechanical energy. The possibilities inherent in the storage of electric energy at 0.25 pound per kilowatt-hour are beyond the horizon and beyond the imagination.

Harnessing of atomic fuel is the most distant goal and it is quite likely that the clue to storage of electric energy at less than 0.25 pound per kilowatt-hour lies at that goal.

The Final Problem

A less tangible horizon is that of public confidence. The performance of structural materials can be predicted, duplicated, and set up in tables. This is not always true of electric apparatus in aircraft. Most people employed in aviation are mechanically minded. They do not understand electricity. To them it is a mysterious phenomenon. They are afraid of it. The news of a failure of an electrical item spreads through the industry, military services, and airline operators at an unbelievable rate. Once spread, all efforts to educate personnel will not eradicate the rumor. Further it is difficult to revise the incorrect operating procedure that may have gone with the failure.

In short, we in the electrical industry must live together. We cannot ignore the troubles of others. The fault of one piece of electric equipment is a discredit to the whole system. D'Artagnan's motto for the three musketeers can well be a slogan for every aviation electrical engineer.

High-Altitude Brush Problem

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THE rapid development of military aircraft during the past three years has been accompanied by an equally vigorous development and use of electric motors and generators necessary for their operation and control. This dynamoelectric equipment covers not only the d-c and a-c applications but also a wide range of power output, voltage, current, and speed. Some of the d-c machines are compensated fully for best commutation, while others are partially compensated, and still others are without compensation and depend on the brush shift and brush grade for satisfactory operation.

The development of carbon and metal-graphite brushes likewise had to keep pace with the development of the electric equipment. At first it seemed that the demand could be met easily, and many standard grades of brushes were used with satisfactory results as long as the planes were flying at low altitudes. However, when the airplanes began to fly at altitudes of 20,000 to 40,000 feet the brushes in many instances were worn out in one to three hours. It became necessary first to determine the causes of the rapid brush wear, and then either to develop treatments for the protection of the existing brush grades, or to make a series of entirely new brush grades capable of withstanding the severe conditions imposed upon them by high-altitude flying, as well as being capable of performing satisfactorily at sea level.

Rapid Brush Wear on Pursuit Planes

In December 1940 a testing program was started in the National Carbon Company laboratory to determine the cause of rapid brush wear encountered in actual flights. The brushes in question are in the motor circuit used for controlling the pitch of the Curtiss variable-pitch propeller. The controlling motor is mounted in the nose or hub of the propeller, which in turn operates the planetary-gear mechanism connected to the blades. The d-c motor is energized through four slip rings which are mounted on the propeller shaft and insulated from it. A stationary brush holder, which is mounted on top of the protective housing, completes the electric circuit to the rings through four rows of small brushes. These are the brushes which were showing rapid wear during flights.

For the first approach of the problem the testing was conducted with equipment made from an actual propeller brush housing and holder (Figure 1). By these means it soon was established that brush temperature and current density had but small effect on brush wear. However, when air with a moisture content of less than 0.15 grain per cubic foot was introduced into the brush housing, rapid brush wear invariably took place after 5 to 30 minutes. Under these conditions the operation of the brushes was accompanied by a loud grinding noise, and a cloud of brush dust could be seen rising from the top of the brush holder. The brushes actually were ground to dust, and in 30 to 60 minutes all of the available brush length was gone.

To meet this problem, two organic lubricants, "*A*" and "12," were developed to be used in conjunction with grade 840-*K* (a copper-graphite material) which was then standard on this equipment. The brushes so treated were designated as *A*-840*K* and 840*K*-12. These two organic treatments provided boundary lubrication on the propeller rings and protected the brushes from rapid wear throughout their life in dry-air atmosphere.

The laboratory findings were checked in actual airplane flights.* For this purpose 15 pursuit ships were used, some of which were equipped with grade 840*K* and the remainder with *A*-840*K* and 840*K*-12. Seventeen sets of 840*K* brushes were worn out, some of them within two hours of flying time; whereas grades *A*-840*K* and 840*K*-12 operated satisfactorily. In one instance grade *A*-840*K* was tested for 112 hours of flying time, and the brushes were not worn out.

This was our first experience with rapid brush wear encountered on military planes. The quick solution provided by the introduction of grades *A*-840*K* and 840*K*-12 was only the beginning of the intensive research which was to follow in the study of the high-altitude performance of carbon and metal-graphite brushes. In the remainder of this paper is given the description of the test equipment used, the results obtained, the theoretical evaluation of the high-altitude problem, and finally its present solution.

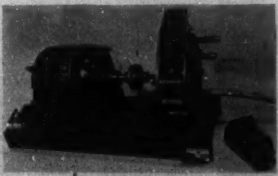

Figure 1. Brush-testing unit for Curtiss variable-pitch propeller

The Atmosphere at High Altitudes

The composition of pure dry air by volume is 78 per cent nitrogen, 21 per cent oxygen, and 1 per cent of ten other gases.[1] These proportions have been found to be essentially the same irrespective of location and for altitudes up to 20 miles. As the altitude is increased there is rapid drop in air pressure, temperature, and moisture content. This is clearly illustrated in Figure 2 in which curve *A* shows the change in air pressure with altitude and curve *B* the change in air temperature and dew point for 100 per cent relative humidity with altitude. The dotted curves C_1 and C_2 indicate respectively the highest and lowest dew-point temperatures for altitudes between 30,000 and 34,000 feet. The data for these two curves were obtained by actual dew-point measurements made in flights at Patterson Field by the United States Air Corps.

In Figure 3 curves *A*, *B*, and *C* show respectively the partial pressure of nitrogen, oxygen, and saturated water vapor with altitude. Curve *D* represents the variation of moisture content of saturated air with altitude. The data for this curve were based on the temperature–altitude curve *B* of Figure 2. At 34,000-feet elevation the air pressure is one fourth of the sea-level pressure, and for this reason the partial pressure of oxygen is reduced by the same factor. The moisture content of saturated air from curve *D* corresponding

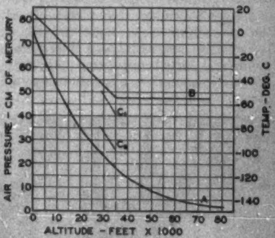

Figure 2

A. Air pressure versus altitude
B. Air temperature versus altitude

Curves *A* and *B* are based on National Advisory Committee for Aeronautics data. Curves C_1 and C_2 are highest and lowest dew-point temperatures measured at Patterson Field

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* The results of these test flights were given to us through the courtesy of M. R. Cushman, engineering department, Curtiss Wright Corporation, Newark, N. J.

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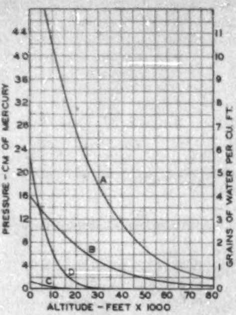

Figure 3

A, B, and *C* represent respectively the partial pressures with altitude of nitrogen, oxygen, and saturated aqueous vapor. *D* is the weight in grains of a cubic foot of saturated aqueous vapor versus altitude. All curves are based on National Advisory Committee for Aeronautics data

to 34,000 feet is 0.015 grain per cubic foot. To complete our picture of the atmosphere at high altitudes, that is, 30,000 to 50,000 feet, it may not be out of place to mention that the ultraviolet rays are strong enough to cause the formation of small amounts of ozone and nitrous oxides.

Experimental

Vacuum Chambers

For the study of the operating properties of carbon brushes at reduced pressures and in various atmospheres, two vacuum chambers were used. Both chambers and the auxiliary equipment are shown in Figure 4. Since the two chambers are practically identical in construction it will be sufficient to describe the one on the left which is visible in greater detail.

The vacuum chamber consists of a glass cylinder which is sealed with rubber gaskets between two steel plates. Inside the chamber on a shaft protruding through the supporting back steel plate is mounted a slotted copper ring. This ring, which is insulated from the shaft, is often referred to as a "dummy commutator." There are four radial brush holders symmetrically spaced around the ring. The brush size is two by three-fourths by three-eighths inches. The brush-spring tension is adjustable and can be varied from 0 to 15 pounds per square inch. The ring is 6.125 inches in diameter and is driven by a variable-speed d-c motor.

The outside air is kept from entering the chamber by a special seal on the shaft. A low-voltage d-c source provides the electric current which flows between the brushes and the dummy commutator. The chamber can be closed and pumped to a pressure of 0.5 millimeter of mercury in five to ten minutes. The pressure then can be regulated by a needle valve which controls the flow of dry air into the chamber. The dew point of the dry air used is below −70 degrees centigrade. If desired other gases than air may be allowed to enter the chamber.

The brush temperature is measured by a potentiometer connected to thermocouples inserted in the brushes close to the rubbing surface.

Brush Wear in Various Gases at Atmospheric Pressure

It was already mentioned that carbon or metal-graphite brushes can be made to wear rapidly or dust at atmospheric pressure if the surrounding air does not contain over 0.15 grain of water per cubic foot of air. Such rapid wear in dry air can be stopped either by increasing its moisture content or by increasing its oxygen content over the amount normally contained in the atmosphere.

Brushes wear rapidly also if the surrounding atmosphere is made up of any of the following gases in dry form: nitrogen, hydrogen, carbon dioxide, and argon. Rapid brush wear or dusting does not take place in ordinary room air mixed either with ozone gas or carbon tetrachloride vapor, although the latter does double the brush friction. In general, contaminants present in the atmosphere in the form of fumes and vapors will tend to prevent rather than bring about rapid brush wear.

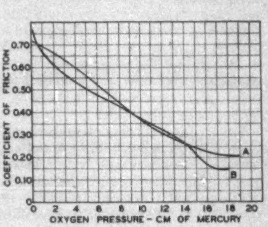

Figure 5

A. Coefficient of friction versus oxygen pressure for graphitized carbon brushes

B. Coefficient of friction versus oxygen pressure for carbon–copper brushes

Brush-spring pressure—11.0 pounds per square inch

Effect of Oxygen, Water Vapor, and Temperature on the Coefficient of Friction and Wear of Brushes

For the experiments with oxygen and water-vapor atmospheres a uniform technique was used. Before each experiment the brushes were heated to a temperature of 300 to 350 degrees centigrade to free them from absorbed moisture. The commutator was heated to 135 degrees centigrade and then quickly resurfaced with a fine-grained commutator stone. The dust was blown away, the brushes were installed, and the chamber was closed. In about ten minutes the chamber was evacuated to a pressure of one millimeter of mercury.

In the experiments with oxygen a steady flow of dry oxygen with a dew point below −40 degrees centigrade was maintained. The rate of flow was limited only by the capacity of the pump at the pressure desired. When water vapor was used the chamber first was evacuated and then water vapor admitted from an evacuated flask. One straight and one inclined mercury gauge were used to

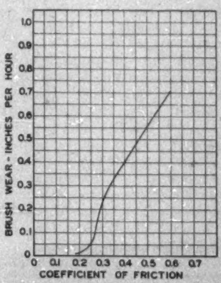

Figure 6. Brush wear versus coefficient of friction for graphitized carbon brushes in dry oxygen atmosphere

Brush-spring pressure—11.0 pounds per square inch

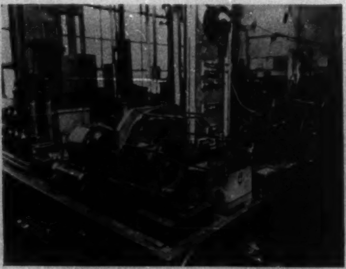

Figure 4. Vacuum chambers used to study the operating characteristics of carbon and metal-graphite brushes at reduced pressures and in various atmospheres

measure the pressure in the chamber.

For the brush-wear measurements a gauge accurate to one-ten-thousandth of an inch was used. Brush-wear measurements were found to be consistent and easy to reproduce. All of the data plotted in Figures 5 to 9 were obtained with a brush-spring pressure of 11 pounds per square inch.

The speed of the dummy commutator purposely was kept low and constant at 1,000 rpm, giving a rubbing speed of 1,600 feet per minute. The driving d-c shunt motor was connected to a constant voltage supply. This made it possible to maintain constant field excitation and use the armature current for measuremeht of friction.

For the experiments in oxygen and water-vapor atmospheres, two kinds of brushes were used. One was a graphitized carbon grade and the other a carbon grade containing 50 per cent copper. The data for all curves were obtained without the flow of current through the brushes. Brush wear and frictional measurements in an oxygen atmosphere present explosion hazards if electric current is permitted to flow from brushes to commutator.

Tests in Dry Oxygen Atmosphere. The relation between coefficient of friction and oxygen pressure for graphitized carbon and carbon–copper brushes is shown in Figure 5. The differences in coefficient of friction for a given oxygen pressure between curves *A* and *B* are small. The final value of the coefficient of friction for the carbon–copper brushes, curve *B*, is lower than that of the graphitized carbon brushes.

The curves *A* and *B* in Figure 5 would be only of academic value if the relationship between the coefficient of friction and brush wear were not known. This relationship is shown in Figure 6 which is for graphitized carbon brushes. A similar relationship exists for the carbon–copper brushes. The data for this curve were obtained by measuring brush wear and coefficient of friction for different oxygen pressures in the chamber and at as nearly constant brush temperatures as it was possible to obtain. For values of the coefficient of friction between 0.3 and 0.6 brush wear is directly proportional to it. When the coefficient of friction is less than 0.3 brush wear decreases at a faster rate and becomes normal with a coefficient of 0.17. This curve shows further that brush-wear values are large, particularly if one bears in mind that the available brush length for wear in actual practice is one-fourth to three-eighths inch.

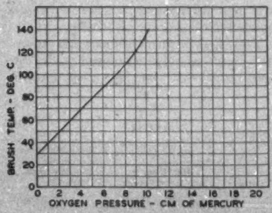

Figure 7. Oxygen pressure at which dusting or rapid wear begins versus brush temperature for graphitized carbon brushes

Brush-spring pressure—11.0 pounds per square inch

Of interest in the experiments with oxygen atmosphere is the relationship between the brush temperature and the oxygen pressure at which rapid brush wear begins.

The curve in Figure 7 shows that at an oxygen pressure greater than 1.1 centimeters of mercury rapid brush wear will not begin with a brush temperature of 40 degrees centigrade. When the brush temperature reaches 130 degrees centigrade an oxygen pressure greater than 9.7 centimeters of mercury is necessary to prevent rapid brush wear. A brush temperature of 130 degrees centigrade corresponds to a commutator temperature of about 100 degrees centigrade. The apparent discrepancy in the amount of oxygen pressure necessary to stop rapid brush wear at the two widely different brush temperatures is undoubtedly due to the different amounts of condensed water vapor on the commutator surface. When the brush temperature increases beyond 130 degrees centigrade any condensed water vapor is quickly removed from the commutator surface, and the oxygen pressure necessary to prevent rapid brush wear or dusting approaches a constant value.

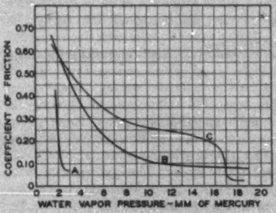

Figure 8. Coefficient of friction versus vapor pressure of water

A. Average brush temperature—60 degrees centigrade

B. Average brush temperature—135 degrees centigrade

C. Average brush temperature—115 degrees centigrade

A and *B* are for graphitized carbon; *C* is for carbon–copper. Brush-spring pressure—11.0 pounds per square inch

The data for this curve were obtained in the following manner: With the brushes and commutator at room temperature, the vacuum chamber was pumped to a pressure of one millimeter of mercury and the commutator brought up to 1,000 rpm. Immediately the brushes began to wear rapidly or dust. Since the brush friction under these conditions was large, the brushes and commutator began to heat up quickly. The commutator temperature was lagging behind the brush temperature about 25 to 30 degrees centigrade.

As soon as the brushes began to dust enough oxygen was allowed in the chamber to stop the rapid wear. At this point the oxygen flow was interrupted while the pump was gradually reducing the oxygen pressure in the chamber. When rapid brush wear or dusting began once more the oxygen pressure and brush temperature were recorded. Again enough oxygen was admitted into the chamber to stop the rapid brush wear, and the cycle was repeated. With each succeeding cycle the brush and commutator temperature increased, and thus it was possible to obtain the data of brush temperature and oxygen pressure at which rapid brush wear began.

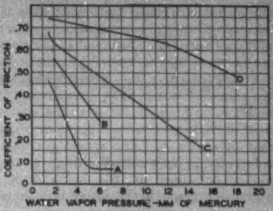

Figure 9. Coefficient of friction versus water-vapor pressure

A. Average brush temperature of 80 degrees centigrade

B. Average brush temperature of 110 degrees centigrade

C. Average brush temperature of 140 degrees centigrade

D. Average brush temperature of 170 degrees centigrade

A, B, C, and *D* are for carbon–copper brushes

Test in Water-Vapor Atmosphere. In water vapor the differences in coefficient of friction for carbon and carbon–copper brushes are more pronounced than in oxygen atmosphere. In Figure 8, curves *A* and *B* show the relationship of coefficient of friction versus vapor pressure of water for graphitized carbon brushes. Curve *C* is for carbon–copper brushes.

Curve *A* was obtained with a brush temperature of 60 degrees centigrade. In this case a pressure of water vapor of 2.9 millimeters of mercury is sufficient to lower the coefficient of friction and brush wear to normal. Curve *B* was obtained with a brush temperature of 135 degrees centigrade and requires 19 millimeters of mercury of water vapor to bring the coefficient of friction and brush wear to normal. Curve *C* was obtained with an average brush temperature of 115 degrees centigrade which is lower than the temperature for curve *B*. Nevertheless, for the range of water-vapor pressures of 2 to 17 millimeters of mercury, the coefficient of friction of curve *C* is greater than that of curve *B*. The reason for that difference is undoubtedly due to the copper contained in the brush which penetrates through the water film more readily.

The effect of brush temperature on the coefficient of friction of carbon–copper brushes in water vapor is perhaps better illustrated in Figure 9.[4] Curves *A*, *B*, *C*, and *D* were obtained with the respective brush temperatures of 80, 110, 140, and 170 degrees centigrade. With a brush temperature of 170 degrees centigrade, 18 millimeters of mercury of water vapor had but little effect on the coefficient of friction and rapid wear of the brushes.

Discussion of Results. The effect of oxygen on the coefficient of friction of nickel sliding on tungsten has been reported by Bowden and Hughes.[2] They found that oxygen at a pressure of 10^{-3} millimeters of mercury was sufficient to lower the coefficient of friction for nickel sliding on tungsten from 6.0 to 1.6. In this instance an attempt was made to obtain photoelectrically clean surfaces.

Experiments with photoelectrically clean carbon sliding on copper will be very difficult to perform. Carbon is heavily loaded with gas. Furthermore, some provision will have to be made to bake completely the chamber, copper commutator, and brushes under good vacuum at a temperature of 500 to 600 degrees centigrade to drive out the adsorbed moisture. The problem, however, is very much simplified by the rapid wear of the brushes. After the brushes have been heated to a temperature of 300 to 350 degrees centigrade and the commutator to a temperature of 150 degrees centigrade or higher, which is sufficient to drive off absorbed moisture, the adsorbed moisture and gases easily can be removed in vacuum as soon as the brushes begin to wear rapidly. Under such conditions films of contamination which might be on the commutator surface are removed in a few seconds. The flow of oxygen which is always maintained in the chamber helps to sweep away contaminants which otherwise may be present.

There is some evidence to show that the oxygen gas protects the sliding surfaces of brushes and commutator from rapid wear, not only by adsorption but also by oxidation.

Water vapor is known to be a good boundary lubricant. On stationary surfaces adsorbed water films are difficult to drive off. On a sliding surface such as a commutator surface, particularly when its temperature is above 100 degrees centigrade, the adsorbed water films can be removed by the brushes in a few seconds.

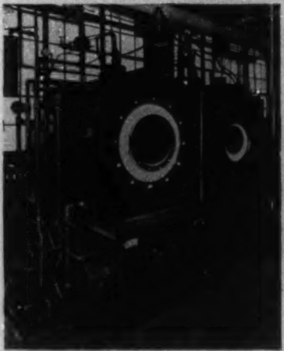

Figure 10. High-altitude chambers used for testing of Amplidynes, motor alternators, inductor alternators, inverters, dynamotors, and other dynamoelectric machines of the motor generator type

Water is ten times as effective as oxygen in reducing the coefficient of friction and preventing rapid brush wear. In general, oxygen pressure of 180 millimeters of mercury is sufficient to prevent rapid brush wear, while water vapor at 18 millimeters of mercury will be equally effective.

Water vapor is most effective on a cold commutator. With graphitized carbon brushes and brush temperature of 60 degrees centigrade water vapor at a pressure of 3 millimeters of mercury is equally effective as water vapor at a pressure of 19 millimeters of mercury when the brush temperature is 135 degrees centigrade. The commutator temperatures are lower than the respective brush temperatures.

As stated before, the data for the curves in Figures 5, 6, 7, 8, and 9 were obtained with two kinds of brushes: one a graphitized carbon grade, and the other a carbon grade containing 50 per cent copper. While the coefficient of friction and rate of wear will be different for brushes of different composition and processing, all brush grades which are not high-altitude type have one thing in common; namely, they wear rapidly, and the coefficient of friction is high under the test conditions previously described.

THE COMMUTATOR FILM

From the description which has been given of the experiments, it is evident that carbon or carbon–copper brushes will operate on a copper commutator with small brush and commutator wear only if there is a film of separation between the rubbing surfaces. This film consists of copper oxide, carbon, adsorbed oxygen, adsorbed water, and contaminants from the air. The contaminants often can be of organic nature such as oil vapors. Once this film is removed the rubbing surfaces of brushes and commutator seize, and quick abrasion of both takes place.

From the fundamental studies of W. B. Hardy[3] we know that when two clean surfaces are brought together within a very close range actual seizure or welding takes place at the points of contact. This seizure is caused by the powerful attractive forces due to the surface atoms. It is further known that these attractive forces are effective only within a very short range, and therefore a boundary film of separation is sufficient to reduce their value to the point where actual welding at the points of contact will not take place. Thus, the whole problem of satisfactory brush operation is reduced to that of boundary lubrication.

At sea level with normal humidity a boundary lubricating film of adsorbed moisture and oxygen is constantly covering the commutator surface. In extremely dry weather, at either sea level or high altitudes, this is no longer possible, and rapid brush wear may follow.

The boundary film on the commutator surface can be organic or inorganic. This film will have to be deposited by some means on the commutator surface throughout the life of the brushes. In our solution of the problem it was found best to let the brushes deposit the film. This means that the brushes for high-altitude applications must be processed specially.

In the use of organic films the treatments or processes known as "*A*" and "12" were already mentioned. These two treatments were developed for use on rings. For brushes used on commutators a complex organic lubricant was developed which was designated as "21." This lubricant has low vapor pressure at temperatures as high as 200 degrees centigrade; it is resistant to oxidation; and, what is most important, it provides a film which gives satisfactory brush life, contact drop, and coefficient of friction under high-altitude conditions. This film also does not introduce periodic contact drop fluctuations frequently referred to as cycling. The "21" lubricant operates best with metal-graphite and resin-bonded natural-graphite brushes.

For the deposition of inorganic protective films a whole series of new grades has been developed ranging from electro-

Table I

Grade	Per Cent Copper Content	Resistivity in Ohm-Inch
ANK	75	0.0000091
ALP	65	0.000019
AJT	45	0.00035
AJZ	25	0.00055
AJS	0	0.0023
AMY	0	0.00040
AMZ	0	0.0025

graphitic brushes to metal-graphite brushes containing more than 70 per cent copper. Some of the brush grades developed for high-altitude applications are given in Table I. The operating temperatures which these grades will withstand are limited not by the brushes but by the machines themselves.

Performance of Grade *AJT* in the Vacuum Chamber

Grade *AJT* is a copper-graphite grade containing 45 per cent copper. It was selected as an example because it is typical of all grades with the fine protection it has against rapid wear at reduced pressures. Table II gives brush life and the coefficient of friction in room air at about 50 per cent humidity, room air at 100 per cent humidity, dry air at atmospheric pressure, dry air at 18.2 centimeters of mercury, and vacuum at 0.1 centimeter of mercury.

The amount of copper used in the various grades listed in Table I has a direct effect on the contact drop with which they will operate at a given current density. Excellent brush life, however, has been obtained with all grades under similar test conditions as in Table II.

The High-Altitude Chamber

So far this paper has presented the operation of brushes on copper rings. The final test had to be made on actual electric machines at air pressure, temperature, and dew-point conditions simulating those at high altitudes.

The general appearance of the two high-altitude chambers, which were constructed for tests of dynamoelectric equipment, is shown in Figure 10. The chamber on the left has its front insulating cover removed in order to show the bolted head which closes the chamber. These two chambers which are alike have worked so well from the time of their construction that a brief description may not be out of place for the sake of those who might be interested.

The chamber was constructed from steel pipe 16 inches in diameter and 18 inches long. One end of the pipe is closed by a welded steel plate, and the other end is welded to a flange to which the front cover is bolted. To increase the cooling surface of the chamber on the inside, longitudinal radial steel fins have been welded around the periphery. The chamber is jacketed with a cylindrical steel case forming an annular space for holding a mixture of dry ice and high-boiling-point naphtha. The steel chamber is housed in a wooden box with four inches of heat-insulating material between the steel jacket and the box walls. It is important that the chambers be well constructed to prevent any leakage of room air which would cause frost deposition in the chamber and lead to erratic results.

Table II

Grade *AJT*

Hours Per Inch	Coefficient of Friction	Atmosphere
6,000	0.21	Room air, 50 per cent humidity
38,000	0.25	Room air, 100 per cent humidity
1,200	0.18	Dry air, sea level
1,500	0.19	Dry air, 18.2 centimeters mercury
25,000	0.12	Vacuum, 0.1 centimeter mercury

Under all conditions of operation the chambers are constantly flushed with filtered dry air, the dew point of which is −70 degrees centigrade. This air is never recirculated. The amount of dry air admitted is limited by the capacity of the pump at the pressure at which the chambers are operated. At an air pressure of 19 centimeters of mercury which corresponds to an altitude of 34,000 feet, the air in the chamber is changed 10 to 20 times per hour. At no time is there any frost visible on the inside walls of the chambers. The importance of taking this precaution hardly can be overstressed. This is undoubtedly one of the reasons why test results obtained have always been in close agreement with actual airplane tests made at Patterson Field.

The walls of the chambers are maintained at a temperature of −70 degrees centigrade, although the inside air temperature may rise up to −50 or −40 degrees centigrade, depending on the amount of heat dissipated by the unit under test. The surrounding cold walls help to freeze the impurities which might be given off by the windings of the machines under test. Most of these impurities naturally are swept out by the dry air which flushes the chambers. The low wall temperature also provides a continuous check of the dew point of the air entering the chamber. As long as there is no frost deposited on the chamber walls it is certain that the dew point of the air is lower than −70 degrees centigrade.

Tests in High-Altitude Chambers

The machines tested in the high-altitude chambers cover a wide variety of types and makes. These included Amplidynes, motor alternators, inverters, dynamotors, inductor alternators, and various other types of small machines. The procedure generally used was to operate the machines in room air with the brushes with which they were received and check their input and output ratings. The length of the room air test varied from 24 hours to several days. After this they were tested in the chambers under the high-altitude conditions as described in the preceding section. The load conditions were varied for the different machines. To simulate the actual working load, the Amplidynes were operated under a cyclic load schedule of 10 minutes full load and 50 minutes no load each hour, while the dynamotors carried a full load continuously. On the other hand, it was necessary to operate the inverters and alternators at 50 to 75 per cent of their sea-level rating to prevent excessive heating under the high-altitude conditions. In many instances when the machines were supplied with brushes which had little or no high-altitude protection the brushes wore out in one to five hours. After this the commutators were resurfaced, high-altitude brushes installed, and the tests repeated. Table III gives the life obtained with ordinary and with high-altitude brushes on these machines in the high-altitude chambers.

With some small machines, the output current of which is only a few milliamperes, the commutators and brushes operate at temperatures considerably lower than 100 degrees centigrade. The brushes of such machines even if they do not have high altitude protection may not wear rapidly for several hours or even days because of the effect of condensed moisture on the commutator and quite often because of the condensation of contaminants given off by the windings of the machine.

Effect of Sparking on Brush Life

In the introduction it was mentioned that the machines used today on military airplanes can be divided from the point of view of compensation into three general groups: completely compensated, partially compensated, and without compensation. This has made it necessary to consider carefully the selection of proper brush grades for satisfactory commutation at sea level. Machines without compensation usually use carbon brushes with or without a small amount of copper.

Table III

	High-Altitude Brushes		Ordinary Brushes
	Brush Grade	Brush Life in Hours for $^1/_4$-Inch Wear	Brush Life in Hours for $^1/_4$-Inch Wear
Amplidyne			
Input	*AJT*	250	4
Output	*AJT*	300	4
Dynamotor			
Input	*AJT*	1,100	12
Low-voltage output	*ALP*	960	19
Medium-voltage output	*AJS*	2,100	162
High-voltage output	*AJS*	5,500	119
Inverter			
Input	*AMZ*	1,200	0.9
Inverter			
Input	*AJZ*	214	0.5
Output	*AJT*	346	130
Alternator			
Input	*T*-171	240	0.8
Rings	*AME*	1,350	440
Inductor Alternator			
Input	*AJZ*	520	6

The partially or fully compensated machines often use copper-graphite brushes with a copper content of 50 per cent or more.

Electric machines which operate satisfactorily without sparking or with minute sparking at sea level may not necessarily perform the same way under high-altitude conditions. The reason for this is that the contact drop of the brushes decreases with a reduction of air pressure and may not be sufficient to prevent the flow of large circulating currents and intensive sparking. Since high-altitude brushes depend on their satisfactory performance on the boundary film deposited on the commutator, any injury caused to this film by intensive sparking naturally will reflect on brush wear and general brush performance. For this reason any compensation or machine adjustment which designers of electric machines can make to reduce or eliminate sparking will increase the likelihood of successful brush performance under high-altitude conditions.

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Instrumentation of 400-Cycle Aircraft Electric Systems

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THE use of 400-cycle alternating current[1] as a power source on large aircraft originates the problem of adequate instrumentation at the distribution panel. Load conditions on a d-c system are determined by the simple parameters of voltage and current, whereas those of the a-c system include frequency and active and reactive power, in addition to voltage and current. Specifically, the flight engineer will require the following information for intelligent operation of the system:

1. Voltage indication, to show whether or not a machine is operating, and as a guide in the adjustment of the voltage regulator.

2. Current indication, to determine whether or not a machine is overloaded. (Overload also can be detected from voltmeter and watt–varmeter indications, but this requires considerable mental effort on the part of the flight engineer.)

3. Active- and reactive-power indication (where two or more generators are operated in parallel), to guide the adjustment of governors for active load division and adjustment of regulators for reactive load division.

4. Frequency indication, to guide the remote adjustment of governors for frequency level where provision for such adjustment has been made.

To summarize, a group of four instruments, namely, ammeter, voltmeter, watt–varmeter, and frequency indicator, together with their associated resistors and current transformer, is desired for use in a-c systems where parallel operation of several alternators is required. Where nonparallel operation is used, either the watt–varmeter or ammeter can be eliminated. For installations using several alternators, duplication of certain instruments such as voltmeters and frequency instruments can be avoided by use of transfer switches. Normally it is expected that it will be necessary to read only one phase voltage, current, and power in each machine. This group of instruments will give the flight engineer reasonably complete knowledge of circuit conditions. In addition to their specific uses, they aid in the location and correction of minor faults and thus prevent major ones. They serve the flight engineer in much the same manner as the flight instruments serve the pilot.

General Requirements—What Size and Weight?

To be specific, we will consider the distribution system of a three-phase 208-wye/120-volt 30-kw 40-kva 400-cycle alternator and determine the minimum size and weight which will characterize a satisfactory group of instruments. All instruments will have a body diameter of $2^{1}/_{4}$ inches, standard aircraft flange-mounting facilities, with depths behind the panel to suit the individual elements. All instruments will have scale lengths of approximately 90 angular degrees and a general accuracy rating of two per cent of full-scale value. They must be designed to withstand satisfactorily the vibration, changes in temperature, air density, and humidity which may be encountered in service. Reasonable independence from load conditions, excepting the particular condition under measurement, is also essential. This may be described as the *self-isolating* property of an instrument by which wattmeter indications are unaffected by frequency change, frequency-meter indications are unaffected by vibration or voltage change, and so forth. These, in brief, are the general requirements on which the designs were based.

Measurement of Current and Voltages

Small-panel a-c moving-iron[2] voltmeters and ammeters have been available from American manufacturers for some years. The investigational work thus was limited to the selection of a mechanism of suitable size, as shown in Figure 1, its enclosure in the standard $2^{1}/_{4}$-inch case, and determination of its characteristics in relation to the requirements. The instrument constants and conditional errors, as shown in Table I, are believed to be consistent with an accuracy rating of two per cent of the full-scale reading.

Instrument Current Transformer

The continuous current rating of the 40-kva alternators is 111 amperes, and an ammeter-scale capacity of 250 amperes was chosen to provide liberally for overload conditions, which are of prime importance to the flight engineer.

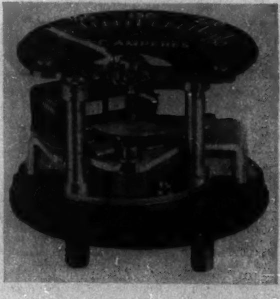

Figure 1. Small-panel $2^{1}/_{4}$-inch moving-iron ammeter, cover removed

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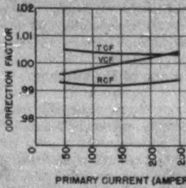

Figure 2. Curves showing correction factors for 400-cycle 125:1 current transformer for line power factor of 0.75, with burden of ammeter, watt–varmeter, and voltage regulator

Ratio correction factor (*RCF*) applies to ammeter reading. Transformer correction factor (*TCF*) applies to wattmeter reading. Varmeter correction factor (*VCF*) applies to var indication when the external resistor furnished with the watt–varmeter is used

To avoid the necessity of running power leads to the instruments and voltage regulator, and to make possible lightweight instrument designs, a special 125:1 current transformer of minimum weight was designed for this application. For simplicity of construction and economy of material, the window type was chosen. This type, with the current magnitudes involved, gave a low number of ampere turns which at 60 cycles would have required a rather heavy core. At 400 cycles, however, the flux required was low enough to permit use of a small core. Further advantage was obtained by using a high-permeability nickel-alloy core material wound in a continuous spiral so as to avoid joints. The secondary coil was wound toroidally on this core, thus practically eliminating any effect of leakage flux.

In order to achieve the minimum weight in this aircraft current transformer, it was designed and compensated for the particular burden with which it was to be used. Furthermore, the ratio and phase-angle errors were made largely to offset each other, thus giving a small wattmeter error, even though the phase angle was larger than generally allowed in instrument transformers. These design principles permit the core to be of much smaller cross section than is the case when one transformer must be capable of supplying a range of burdens. Curves of ratio correction factor (*RCF*), transformer correction factor (*TCF*), and var correction factor (*VCF*) are shown in Figure 2. Of course, a transformer designed for a specific burden would be less accurate if used for other burdens, but that does not matter when the burden is definitely known in advance. Similar transformers for other burdens can be made readily by minor modifications of design.

The weight of the 400-cycle transformer, illustrated in Figure 3, is 0.43 pound. This weight advantage results not only from the measures just described but also from the method of support, consisting of a punched aluminum frame over which the winding is placed as shown in the illustration. A transformer of the same type and material for use on 60-cycle circuits would weigh about three or four pounds. The smallest usual 60-cycle current transformer of equivalent current rating weighs about 13 pounds.

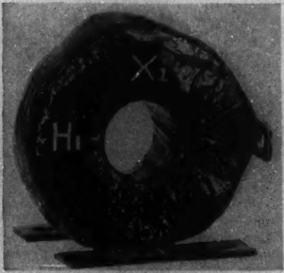

Figure 3. Window-type 400-cycle current transformer for aircraft, $2^1/_2$-inch diameter

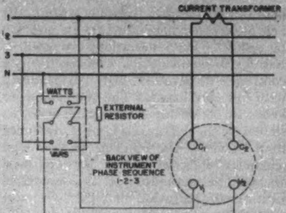

Figure 4. External-connection diagram for watt–varmeter

Table I. Constants and Performance Data of $2^1/_4$-Inch 0–2.0-Ampere Ammeters and 0–250-Volt Voltmeters

Average of 16 Samples. Normal Accuracy—Two Per Cent of Full-Scale Value. Weight—0.37 Pound Each

Scale Constants		
1. Length (inches)	1.52	
2. Angle of deflection (degrees)	85.1	
Moving Element		
1. Full-scale torque (T) (millimeter grams)	0.24	
2. Mechanical factor of merit (T/W)	0.52	
3. Response time (seconds)	1.30	
Volt-Ampere Burden		
1. Voltmeter at 250 volts (va)	0.27	
2. Ammeter at 250/2 amperes (va)	0.20	
Performance Data		
All errors are expressed as a percentage of the full-scale value.		
	Ammeter	**Voltmeter**
1. Full-scale self-heating error	< ±0.3	±0.6
2. Frequency error		
(*a*). 400–450 cycles	< ±0.3	< ±0.3
(*b*). 400–350 cycles	< ±0.3	+0.5
3. Ambient-temperature error		
(*a*). +25 C to +68 C	+0.9	−1.2
(*b*). +25 C to −38 C	−1.3	+1.8
(*c*). Permanent change	±0.3	±0.4
4. Error due to 10 instantaneous applications of 1,000 per cent load to ammeter	±0.6	
5. Effect of 95 per cent relative humidity at 65 C for 6 hours	±0.3	±0.5
6. Effect of vibration at 500–3,000 cycles per minute 0.020-inch amplitude	< ±0.3	< ±0.3

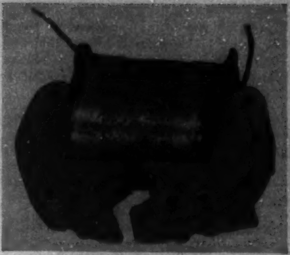

Figure 5. Ferromagnetic field structure of watt–varmeter with field-coil winding

Measurement of Active and Reactive Power

Since major loads are balanced and the system neutral is grounded, considerable saving in weight was achieved by the adoption of a single-phase element with the cross-phase connection for the measurement of vars as shown in Figure 4. In selecting the basic element, the following types were considered:

(*a*). Electrodynamic—iron core.
(*b*). Electrodynamic—air core.
(*c*). Induction-disk type.

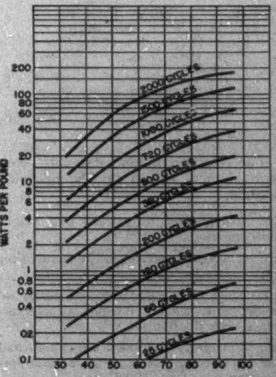

Figure 6. Curves showing typical core loss versus frequency for Nicaloi

Table II. Constants and Performance Data of $2^1/_4$-Inch Single-Phase Watt–Varmeter Rated 120 Volts, 1.11 Amperes, 133.3 Calibrating Watts With 0–50-Kw Scale for Three-Phase Four-Wire 208/120-Volt System and 125:1 Current-Transformer Ratio

Average of Seven Samples. Normal Accuracy—Two Per Cent of Full-Scale Value. Weight—0.58 Pound Each Including External Varmeter Resistor

Scale Constants	
1. Length (inches)	1.56
2. Angle of deflection (degrees)	90
Moving System	
1. Full-scale torque (*T*) (millimeter-grams)	0.35
2. Mechanical factor of merit (*T*/*W*)	1.06
3. Damping factor	2.0
4. Response time (seconds)	1.1
Potential Circuit	
1. Total terminal resistance (ohms)	12,000
2. Watts loss	1.20
3. Armature resistance (ohms)	46
4. Armature, reactance 400 cycles (ohms)	3.2
5. Maximum temperature rise of resistor spools (degrees centigrade)	37.5
Current Circuit (Field)	
1. Resistance (ohms)	0.10
2. Watts loss	0.12
3. Volt-ampere burden at rated current	2.5
4. Temperature rise (degrees centigrade)	18
Performance	**Per Cent Full-Scale Value**
1. Self-heating error (120 volts, 1.11 amperes, 400 cycles)	−0.5
2. Frequency error, unity power factor	
(*a*). 400–450 cycles	<−0.3
(*b*). 400–350 cycles	<−0.3
3. Ambient-temperature error	
(*a*). +25 C to +68 C	+1.1
(*b*). +25 C to −38 C	−2.1
(*c*). Permanent change	−0.5
4. Low-power-factor error at rated volt-amperes	
(*a*). 0.8 power factor	−0.5
(*b*). 0.5 power factor	−0.7
5. Varmeter operation	
(*a*). Zero power factor at instrument, rated volt-amperes	±0.9
6. Voltage error	
(*a*). 120–132 volts	+0.3
(*b*). 120–108 volts	−0.3
7. Sustained overload	
(*a*). Six hours at 100 per cent voltage, 120 per cent current	±0.3
8. Error due to 10 instantaneous applications of 1,000 per cent load to current circuit	<±0.3
9. Effect of 95 per cent relative humidity at 65 C for 6 hours	+0.5
10. Effect of vibration for 2 hours at 500–3,000 cycles per minute at 0.020-inch amplitude	±1.0

The electrodynamic iron-core construction was chosen for the following reasons:

(*a*). For a given torque size and weight were minimum.

(*b*). The same mechanism can be used for other measurements, such as frequency.

(*c*). The scale is linear and consistently reproduceable, making possible a predetermined scale distribution.

(*d*). Inherent performance errors are small and can be compensated by known and simple means.

The magnetic structure of the wattmeter, Figure 5, comprises two stacks of laminations which overlap within the field coil and which are held in position by screws. The laminations are made of Nicaloi, 0.004 inch thick, a material which combines moderately high permeability with low core losses. The 400-cycle core losses at 50 kilolines per square inch, as shown by the curves of Figure 6, are more than 18 times the 60-cycle value. The magnetic material within the coil was operated at maximum density compatible with linear saturation characteristics and acceptable volt-ampere burden.

The electric circuit of the wattmeter, Figure 7, is composed of the field or current circuit and the armature or potential circuit. Each circuit is insulated from the other and from ground. The armature circuit includes two adjustable resistors and a reactance unit. These internal resistors provide an adjustment range of ±8 per cent of the instrument reading and are used for calibration adjustment during manufacture. The reactance compensates for the low-power-factor error originating from the phase-angle difference between field flux and field current due to core loss in the magnetic material. It introduces a corresponding phase-angle lag between potential-circuit voltage and armature current. The compensation is very good in the operating frequency range, and the maximum recorded instrument error at any load, zero power factor, did not exceed 1.5 per cent of the full-scale reading. The low-power-factor error is of importance when the instrument is used as a varmeter.

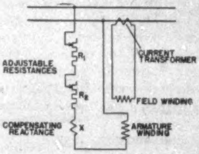

Figure 7. Electric circuit of 400-cycle watt–varmeter

Series reactance is used in the potential circuit to compensate for the phase angle between field flux and field current when measuring vars

Since the reactance of coils at 400 cycles is 6.7 times as great as at 60 cycles, it was necessary to use minimum ampere turns on the field to minimize the volt-ampere burden on the transformer. For this reason, the potential-circuit resistance was designed for 100 ohms per volt. At power frequencies, this resistance could be increased to 1,000 ohms per volt.

The constants and performance of the watt–varmeter, Table II, show reasonable conformity with the objectives of the design. The magnetic damping system, illustrated in Figure 8, consists of a small U-shaped Alnico II magnet and 0.008-inch aluminum disk. This system produces satisfactory dynamic characteristics which are independent of air density and altitude.

Measurement of Frequency

The design of the 120-volt 350–450-cycle frequency meter is based on the principle of series resonance at normal frequency. Of the several methods of application of this principle, the circuit which appeared to yield the minimum weight and size is that illustrated in Figure 9. This circuit consists of the field coil, further shown in Figure 10, and a moving coil connected through capacitor, reactor, and resistor to a secondary winding on the field coil.

The operation of the instrument is explained best by reference to the vector diagram. The field current, I_f, produces a field flux, ϕ_T, which induces the armature-circuit voltage, E_a. The flux ϕ in the air gap at the armature is a portion of ϕ_T. The reaction between ϕ and the armature current, I_a, produces the deflecting torque. At resonance, approximately 400 cycles, the armature current, I_a, is in quadrature with the field flux, and no torque is produced. At 350 cycles the armature current, I_a, leads the armature voltage, E_a, and produces a down-scale deflecting torque proportional to $I_a \sin \alpha$. At 450 cycles the armature current lags the voltage and produces an up-scale deflecting torque also proportional to $I_a \sin \alpha$. These deflecting torques vary with frequency, and true frequency indication is obtained by the addition of a restoring torque which is a function of the line voltage and armature displacement from the center scale position. This restoring torque is produced by a small *Mu*-metal vane[3] fixed to the

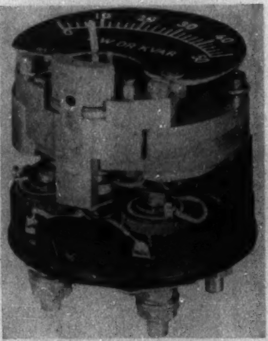

Figure 8. Small-panel $2^1/_4$-inch electrodynamic 400-cycle watt–varmeter with cover removed

The internal adjustable resistors are mounted on the insulating base armature shaft which tends to align itself with the parallel field flux. This construction minimizes the influence of voltage on frequency indication because both the deflecting torque and restoring torque vary with the second power of the voltage.

The transformer shown in the circuit diagram, which consists of two windings on the same spool, is used to energize the armature circuit because the phase angle between E_a and ϕ will not change with increase in field resistance due to self-heating. Since no deflecting torque is produced at resonant frequency, changes in armature resistance do not appreciably affect accuracy at mid-scale frequencies.

Table III. Constants and Performance Data of $2^1/_4$-Inch 120-Volt 350–450-Cycle Self-Contained Frequency Meters

Average of Four Samples. Weight—0.83 Pound Each. Conditional Errors Are Expressed in Cycles

Scale Constants	
1. Length (inches)	1.53
2. Angle of deflection (degrees)	87.5
Moving System	
1. Equivalent* full-scale torque (T) (millimeter grams)	0.43
2. Equivalent mechanical factor of merit (T/W)	0.53
3. Response time‡ (seconds)	1.80
Volt-Ampere Burden at 120 Volts 400 Cycles	
1. Current (amperes)	0.113
2. Volt-amperes	13.6
3. Watts	1.9

Performance	350 Cycles	400 Cycles	450 Cycles
1. Self-heating error at 120 volts	+3.8	+2.8	+1.4
2. Voltage error			
(a). 120–132 volts	+3.3	+0.5	−4.6
(b). 120–108 volts	−3.5	−0.8	+3.6
3. Ambient-temperature error			
(a). +25 C to +68 C	±1.3	±0.9	±1.8
(b). +25 C to −38 C	−5.0	−3.9	−0.7
(c). Permanent change	±0.9	±1.0	±0.9
4. Effect of 95 per cent relative humidity and 65 C for 6 hours	< ±0.3	< ±0.3	±1.1
5. Effect of vibration for 2 hours at 500–3,000 cycles at 0.020-inch amplitude	+0.4	< ±0.3	+0.5

* This is the product of the torque gradient at 400 cycles and the scale angle.

‡ The interval required for the pointer to indicate true frequency when instrument is energized at 400 cycles 120 volts.

Table IV. Weights of Typical Sample $2^1/_4$-Inch Aircraft-Power Panel Instruments

Instrument	Weight in Pounds D-C System	 400-Cycle A-C System
Ammeter (instrument only)	0.40	0.37
Voltmeter	0.41	0.37
Watt-varmeter		0.58
Frequency meter		0.83
Transformer		0.43
Shunt	1.80	
Total	2.61	2.58

The scale distribution, as analyzed in Appendix I, is open and readable throughout the frequency range, although not quite symmetrical with respect to the 400-cycle reference point.

Connections to the armature winding are made through low-torque spirals which are positioned to pull the pointer off scale to the left when the instrument is de-energized. This extraneous torque introduces a small voltage error and slightly changes the resonant-frequency adjustment.

In calibrating the frequency meter three adjustments are required:

1. The restoring torque is determined by the factory setting of the angular position of the armature vane.

2. The deflecting torque is adjusted to give the proper scale length by variation of the series rheostat, the effect of which is shown in Figure 11.

3. The pointer position is set by adjustment of the reactance.

To facilitate calibration, both coarse and fine adjustments are provided. The parallel lines, Figure 12, show that the pointer position can be controlled by this change in reactance without altering the scale length. These adjustments are used in the initial setting of the calibration at the factory.

Magnetic shielding of the frequency meter was necessary to eliminate stray fields which otherwise would produce a phase-angle change between the field flux in the transformer and that acting on the armature.

The characteristics and performance are tabulated in Table III. The minimum torque of the frequency meters is 33 per cent more than that of the wattmeters because of the heavier armature. The ratio of torque to armature weight varies from 0.5 to 1.0, depending on scale position. Ambient-temperature errors are due principally to changes in resistance of the armature circuits, the ohmic resistance of which is approximately 25 per cent copper. Changes in core loss with temperature introduce phase-angle errors. Both sources of error originate from the space limitations of the device. The performance is best in the operating range around 400 cycles.

Figure 10. Ferromagnetic field structure of frequency meter with field-coil winding and internal transformer

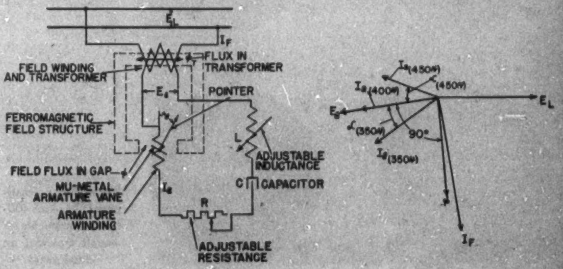

Figure 9. Electric circuit and approximate vector diagram of 400-cycle self-contained $2^1/_4$-inch frequency meter

Operation depends on the unique variation with frequency of the phase angle between the armature current, I_a and the field flux, ϕ

Conclusion

The weight summary of Table IV compares the instrumentation of the 400-cycle a-c system with that of a 27-volt d-c system of equivalent capacity. The major component of the d-c system is the 1,200-ampere 50-millivolt shunt, while the weight of the a-c instrument group is more uniformly distributed with no single value exceeding one pound. The totals are substantially equal, and thus it is shown that the a-c instrumentation, including the measurement of watts, vars, and frequency, by careful design, can be accomplished without weight penalty. The function of instrumentation is the presentation of information in a manner suitable for quick and adequate perception. This factor has been fully recognized by designing instruments of uniform size, method of indication, and scale length, as shown in Figure 13. Uniform dynamic characteristics are obtained by magnetic damping of the moving systems. Performance data, while preliminary in char-

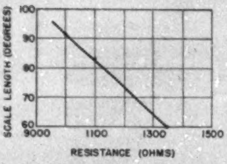

Figure 11. Curve showing scale arc length versus armature-circuit resistance for frequency meter

acter and including neither field nor flight tests, show no serious limitations in either accuracy or durability.

Appendix Calculation of the Scale Distribution of the Frequency Meter

The scale distribution of the frequency meter may be predetermined mathematically from the circuit constants and physical arrangement of the instrument parts shown in Figure 9. The three torques acting on the armature are evaluated and the equation of the scale distribution derived by equating their sum to zero. Numerical solution is obtained by applying the boundary conditions defined by the scale length and frequency range. Comparison of calculated with actual values is shown by the curves of Figure 14.

Assumptions

To simplify the solution the following assumptions are made:

1. The flux, ϕ, in the air gap is uniform, parallel, and normal to the pole faces.

2. The flux, ϕ, in the air gap is in phase with the flux, ϕ_T, in the field-coil transformer.

3. The flux, ϕ, in the air gap leads the armature voltage, E_a, by 90 electrical degrees.

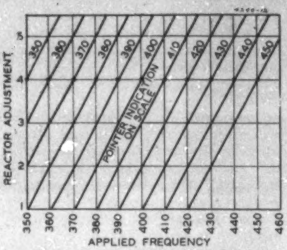

Figure 12. Curves showing range of adjustment for calibrating the frequency meter by changing armature-circuit inductance by means of the adjustable reactor

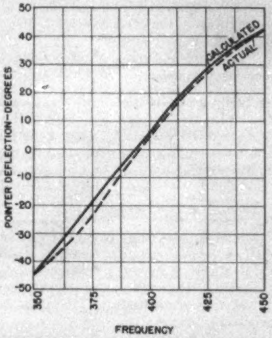

Figure 14. Curves showing actual scale distribution of one sample frequency meter and the theoretical distribution calculated from the known circuit constants and physical arrangement of the instrument parts

4. The moving system is perfectly balanced, and friction is negligible.

Nomenclature

E_L = line voltage (volts)

E_A = armature-circuit voltage (volts)

I_F = field current (amperes)

ϕ_T = field flux induced by I_F (maxwells)

ϕ = field flux effective on armature in gap (maxwells)

N = number of turns on field coils

K = spring constant of spirals (millimeter grams per radian)

K_1, K_2, K_3, K_4, and so forth = constants

I_a = current in armature circuit (amperes)

Z_a = impedance of armature circuit (ohms)

X_L = inductive reactance of armature circuit (ohms)

X_C = capacitive reactance of armature circuit (ohms)

L = inductance of armature circuit (henrys)

C = capacitance in armature circuit (farads)

R_a = armature-circuit resistance (ohms)

S = initial deflection of spiral springs from center scale (radians)

θ = armature deflection (radians) (+ is clockwise). When $\theta = 0$,

(*a*). Pointer is at center scale.

(*b*). Spiral deflection is $+S$.

(*c*). Armature vane is in line with flux ϕ.

(*d*). Armature winding axis is at 90 degrees to flux ϕ.

y = moment arm of armature conductors

f = frequency, cycles per second

T_D = electrodynamic deflecting torque (millimeter grams) (+ is clockwise)

T_V = torque due to armature iron vane (millimeter grams) (+ is clockwise)

T_S = torque due to spirals (millimeter grams) (+ is clockwise)

α = phase angle between armature voltage and current

$\alpha = \tan^{-1}[(X_L - X_C)/R_a]$

Analysis

The electrodynamic deflecting torque, T_D, acting on the armature of the frequency instrument is

$$T_D = K_1 \phi y I_a \sin \alpha \tag{1}$$

The flux, ϕ, in the air gap of the instrument due to the field current, I_F, which reacts with the armature current, I_a, may be expressed as

$$\phi = K_2 \frac{E_L}{fN} \tag{2}$$

The component of the armature current, I_a, which is in phase with the field flux, ϕ, is

$$I_a \sin \alpha = \frac{E_a}{Z_a} \sin \alpha \tag{3}$$

The moment arm of the armature conductor may be written

$$y = K_3 \cos \theta \tag{4}$$

Equations 2, 3, and 4 may be substituted in equation 1:

$$T_D = K_4 \frac{E_L E_a}{fNZ_a} \sin \alpha \cos \theta \tag{5}$$

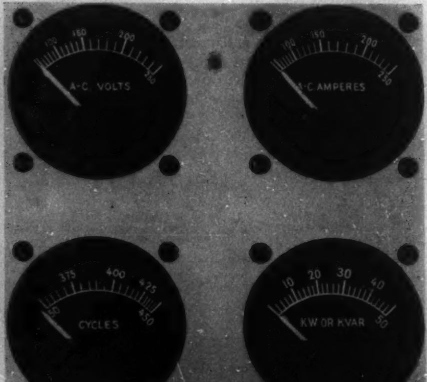

Figure 13. Small-panel $2^1/_4$-inch 400-cycle instruments for aircraft mounted on metal panel

The restoring torque acting on the armature due to its iron vane tending to align itself with the field flux, ϕ, is

$$T_V = -K_5\left(\frac{E_L}{fN}\right)^2 \sin 2\theta \qquad (6)$$

The extraneous torque due to the lead-in spiral springs is

$$T_S = -K(\theta+S) \qquad (7)$$

Since the summation of torques acting on the armature must be zero, for steady-state indication,

$$T_D + T_V + T_S = 0 \qquad (8)$$

If equations 5, 6, and 7 are substituted in equation 8:

$$K_4 \frac{E_L E_a}{fNZ_a} \sin \alpha \cos \theta - K_5\left(\frac{E_L}{fN}\right)^2 \sin 2\theta - K(\theta+S) = 0 \qquad (9)$$

This is the equation for the scale distribution. For the purpose of determining the values of K_4 and K_5, the following boundary conditions are assumed which correspond to the scale length of one of the sample instruments:

1. At $f=350$ cycles the pointer deflection is $\theta = -44$ degrees.
2. At $f=450$ cycles the pointer deflection is $\theta = +43$ degrees.

By substituting these values and the known constants, equation 9 is reduced to the following functions of frequency and deflection:

$$(2.4)10^6 \frac{\sin \alpha \cos \theta}{fZ_a} - (1.56)10^4 \frac{\sin 2\theta}{f^2} - 0.038(\theta+1) = 0 \qquad (10)$$

Equation 10 defines the theoretical scale distribution in terms of pointer deflection, θ; frequency, f; and known constants. The solution to equation 10 is shown by the calculated curve of Figure 14 and is compared to the distribution of one of the sample instruments by the "actual" curve. Deviation between the curves is due mostly to the nonuniform distribution of flux in the air gap.

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A Distance Relay With Adjustable Phase-Angle Discrimination

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DISTANCE-TYPE transmission-line relays have been used very successfully for a number of years. Several different distance-type characteristics have been available. Among these characteristics are the impedance relay which measures impedance substantially independent of the phase angle, the reactance relay which responds only to the reactive component of the impedance, and a third characteristic which responds to a compromise between pure impedance and reactance. Another type of relay characteristic which combines impedance measurement with a directional indication has been used for a number of years as a fault-detector element to supervise another distance-measuring element.

These distance-type relays have been able to cope with most of the conditions encountered on the majority of transmission lines and systems. However, at present, the conditions which the relays must handle are becoming more and more difficult. This is because of increased loading especially on the longer transmission lines. Because of this it is becoming increasingly difficult for distance relays to distinguish between fault conditions and load conditions. It is the purpose of this paper to describe a new relay whose distance-response characteristic can be shifted or moded to fit different conditions. Distance elements which have capacity to discriminate between load currents and fault currents have been available but have been used mostly as fault-detector elements rather than distance-measuring elements to provide primary protection.

The relay to be described has directional discrimination as well as distance-measuring capacity and furthermore its directional discrimination can be regulated from zero to the maximum by the manipulation of suitable taps.

In applying relays to a transmission system it is necessary to state the relay characteristics in the same terms that the system conditions are stated. This is especially true of distance-type relays. If the relay characteristics are thought of in terms of volts and amperes, then the system conditions should be stated in the same terms. With the distance relays, however, it is difficult to think in terms of volts and amperes, because these values vary widely for the same relay response. This relay response is some function of the ratio between volts and amperes and, for any given value of the ratio, there may exist an infinite number of values of the volts and amperes. It therefore simplifies matters greatly to think of the distance-relay response in terms of the ratio of volts to amperes or in other words to think of the impedance, reactance, resistance, or combination thereof to which the relay responds. In applying these relays to a power system it is necessary to think of the system in the same terms. However, in designing distance relays it is necessary to think in terms of the volts, amperes, and phase angle to which it must respond because these are the quantities which actually operate the contact-actuating parts.

When distance relays are applied to transmission lines it is most convenient to plot both the relay characteristics and the transmission line conditions on an impedance diagram, that is, on rectangular co-ordinates of resistance and reactance. This gives a picture of the impedance values for which relay operation is desired, as well as a picture of the impedance values seen by the relay during load and synchronizing surge conditions, for which operation is not desired.

It is interesting to see what the various relay characteristics look like on a current-phase angle basis and what occurs when transfer is made to the R and jX diagram. When the current response against a given voltage is plotted against phase angles, the loci of all distance-type relays whether impedance, reactance, resistance, or any combination thereof are circles if we consider the straight line as the special case of a circle with infinite radius. No attempt here is made to provide rigorous proof of this statement as this has been covered in various publications. It is instructive to examine the various distance-relay characteristics on the current-angle diagram and then transfer them to the resistance and reactance co-ordinates.

With reference to Figure 1, the impedance relay has the response circle shown with its center at the origin. It is obvious that the current locus must be a circle since a pure-impedance relay will operate against a given voltage restraint at the same current value regardless of the phase angle between the current and voltage. To transfer this circular characteristic to the resistance-reactance co-ordinates in Figure 2, it is necessary only to divide the constant-voltage restraint by the current pickup values at different angles and plot the resultant impedance

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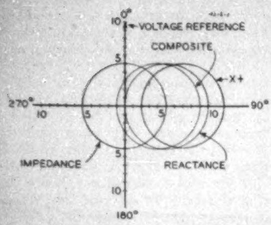

Figure 1. Current–phase-angle diagram of impedance, reactance, and composite characteristics

values at the corresponding angles on the R–X diagram. Obviously the current circle of Figure 1 with its center at the origin is also an impedance circle in Figure 2 with its center at the origin. If the current circle, Figure 1, were to be moved to the right along the 90–270-degree axis until it is tangent to the 0–180-degree axis it then becomes a pure reactance characteristic. Plotting the reciprocal of the current-pickup values given by this circle at different angles on the R–X diagram at corresponding angles gives the straight-line reactance characteristic in Figure 2. Placing the circle at a position between the impedance and reactance circles in Figure 1 gives a composite characteristic depicted by the large circle in Figure 2. Pushing the circle in Figure 1 beyond the reactance circle such as at $X+$ bends the reactance characteristic in Figure 2 upward into the circle shown.

While in the foregoing discussion the characteristic circle was shifted along the X axis, it is to be understood that this shifting process can be performed along any angular axis.

In Figure 1 the current-locus circle was shifted to the right by bodily moving the circle over. The circle can be shifted in another manner, that is, by holding a point on the circle stationary and then expanding its diameter to minus infinity after which its diameter is reduced from positive infinity. Figure 3 shows this transformation where the circles are all passed through the point $I=5$. Transferring these circles to the R–X diagram yields the interesting set of circles in Figure 4. Note that the impedance circle decreases in diameter and moves upward. The movement of the center of the circle away from the origin means that the impedance characteristic acquires a sense of direction. In other words, it means that response to impedance values in some directions is greater than its response in other directions. When the circle becomes tangent to the R axis, it means that the impedance characteristic has become completely directional.

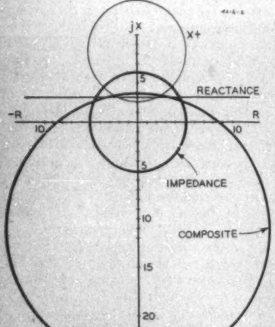

Figure 2. Resistance–reactance diagram of current loci of Figure 1

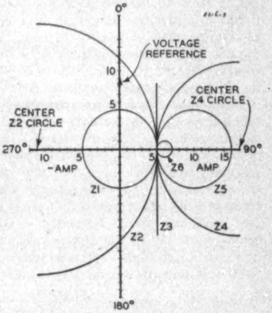

Figure 3. Current–phase-angle diagram of various impedance characteristics

In applying distance relays to transmission lines it is convenient to plot the conditions for which tripping is desired as well as the nontripping conditions on resistance and reactance co-ordinates. It is easy to plot the fault ohms on this diagram as the resistance and reactance ohms of the transmission line are readily available. Thus in Figure 5 the line L_F–L_F' represents the line on which the impedance ohms of the line fall, and the point B represents the ohms for a solid fault at the far bus. The vector L_{FB} represents the apparent impedance value when fault arc resistance is included. On short lines the line ohm vector is the only one to be considered. However, on the longer heavily loaded lines it is usually necessary to consider the impedance values seen by the relays during normal load conditions and during surge conditions when synchronizing current is flowing over the line to keep the generating sources in step. While it is easy to plot the normal-load-impedance vector it is generally much more difficult to plot the values representing momentary load swings and synchronizing surges. The exact location of the impedance vectors for the latter conditions involves considerable calculation but methods for doing this have been covered previously. An approximate idea of the manner in which the synchronizing-surge-impedance vector shifts can be gained by locating the point along the line L_F which represents the number of ohms the electrical center is away from the relay location. The load or surge-impedance vector will terminate in a straight line which intersects the line L_F at the electrical center. The more severe the swing condition the closer this vector will approach the line L_F. Some particular maximum approach to the line L_F will represent the dividing line between ability of the system to stay in step or to develop into a definite out-of-step condition. Generally it is desirable to permit tripping for all surge conditions from which the system cannot recover but to prevent tripping for all load conditions and surges up to this nonrecovering point. The object in applying distance relays to transmission lines is to utilize a relay characteristic that will trip definitely for faults on the line but not trip for swing conditions from which the system can recover. This means that the relay-response characteristic, when plotted on the R–X diagram, must encircle properly the line ohms including fault resistance but must not encircle the swing ohms. Realizing that the minimum synchronizing surge ohms for which it is not desired to trip may fall quite close to the fault-ohm line it becomes evident that a characteristic similar to Figure 5 is necessary. Such a characteristic could enclose the line ohms but exclude the synchronizing-surge ohms if the diameter of the circle, the position of the circle on an axis, and the angular position of the axis were all adjustable.

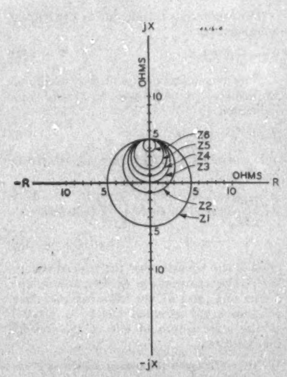

Figure 4. Circles of Figure 3 plotted on R and X co-ordinates

Such a characteristic has been imparted to the beam-type impedance relay by making the restraint proportional not to voltage alone but proportional to the vector sum of the fault voltage and the fault current while the operating force is made proportional to the fault current. Figure 6 shows the schematic connections. The delta current $I_A - I_B$ is applied to the primary winding of a current transformer and to the operating coil on the beam element. The secondary of the current transformer being shunted by the reactor X_4 causes a voltage drop across the resistor R_4 which leads the current by 60 degrees. A portion of the voltage R_4 is introduced into the circuit supplying voltage to the standard split-phase potential-restraint coils of the beam element. The voltage E_{AB} is fed through the phase shifter as shown. As a result of this arrangement the energy fed into the potential-restraint coils of the impedance element is proportional to the vector sum of $\boldsymbol{E}_{AB}$ and $\boldsymbol{I}_A - \boldsymbol{I}_B$.

The action which takes place to cause the element to have a circular characteristic can be seen from the vector diagram in Figure 7. This is drawn for the case where the center of the circle is to be placed on the X axis and the circle is to pass through zero. Assume that the fault current $\boldsymbol{I}_A - \boldsymbol{I}_B = \boldsymbol{I}_R$ lags the fault voltage $\boldsymbol{E}_{AB}$ by 90 degrees and that under this condition the current in the potential-restraint coils due to the fault current $\boldsymbol{I}_R$ is 180 degrees out of phase with the current due to the restraint voltage $\boldsymbol{E}_{AB}$.

The operating energy on the front end of the beam is represented by the vector $\boldsymbol{I}_0$, although this is the same current as that operating on the rear end of the beam, while the current on the front end is shown as a vector, no vector relationship exists between the pull on the front end and the pull on the back end of the beam, since the beam has substantially no response to the phase angle between the forces on the two ends. It is arranged that the current I_0 produces a mechanical force on the front end of the beam equal to the force produced by the current on the back end of the beam when no voltage is present. For fault currents lagging 90 degrees it is easy to establish the conditions for a balance point. For instance, if the voltage $\boldsymbol{E}$ is assumed to be 12 units, then it is evident that the current $\boldsymbol{I}_R$ directly opposite must be 6 units in order for the net restraint of 6 units just to balance an operating force of 6 units. If the current lags the voltage less than 90 degrees (say $\boldsymbol{I}_R'$), it must be increased to a value such that the vector sum of $\boldsymbol{I}_s$ and $\boldsymbol{E}_1$ (or $\boldsymbol{I}_s$) still equals $\boldsymbol{I}_R$ because the operating current I_0 has increased at the same rate, in order to maintain a balance point. It can be seen from an inspection of Figure 7 that the current $\boldsymbol{I}_R$ and $\boldsymbol{I}_0$ for any angle must terminate on the straight line MM in order to make the scalar value of the vector sum $\boldsymbol{E}_1 + \boldsymbol{I}_R = \boldsymbol{I}_s = \boldsymbol{I}_R = \boldsymbol{I}_0$. In other words, the line MM represents the locus of balance currents for a given voltage $\boldsymbol{E}_1$. Division of the voltage $\boldsymbol{E}_1$ by the balance currents gives impedance values, which plotted on R and X co-ordinates gives a circle with its center on the X axis and passing through zero. The pull of the current on the restraint end of the beam must be equal to the pull of the current on the other end in order for the current locus to become a straight line. If the current pulls do not balance the characteristic becomes a circle above or below the straight line depending upon which way the beam is unbalanced so that on current alone one unit of I_0 is required to balance two units of I_R (or $I_0 = 2I_R$), and the characteristic becomes a circle as shown. Under this condition one balance point with voltage applied will be when $12 - I_R = I_0 = 2I_R$ or $I_R = 4$ units. The other balance point is given by $I_R - 12 = I_0 = 2I_R$ or $I_R = -12$. On the R and X diagram this is a circle with its center on the X axis and the origin is within the circle. If $I_0 = I_R/2$, that is, the current on the restraint end of the beam is predominant, then we obtain the circle shown which on the R and X diagram does not contain the origin.

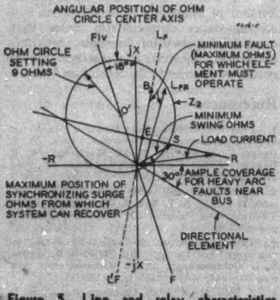
ANGULAR POSITION OF OHM CIRCLE CENTER AXIS
FIV
jX
LF
OHM CIRCLE SETTING 9 OHMS
18°
MINIMUM FAULT (MAXIMUM OHMS) FOR WHICH ELEMENT MUST OPERATE
B
LFR
O'
Z2
MINIMUM SWING OHMS
E
S
LOAD CURRENT
-R
R
30°
AMPLE COVERAGE FOR HEAVY ARC FAULTS NEAR BUS
MAXIMUM POSITION OF SYNCHRONIZING SURGE OHMS FROM WHICH SYSTEM CAN RECOVER
DIRECTIONAL ELEMENT
-jX
L'F
F

Figure 5. Line and relay characteristics plotted on R and X co-ordinates

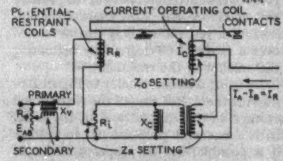

Figure 6. Schematic connections of impedance relay with adjustable characteristics

It is clear from the foregoing that the introduction of current into the voltage-restraint circuit has performed the circle inversion depicted in Figures 3 and 4. However, in these figures the current circle was inverted by holding a point on its periphery fixed. This caused the impedance circle, Figure 4, to diminish in diameter as it was shifted. It is, of course, desirable that the impedance-circle diameter remain constant while the center is shifted. The arrangement shown in Figure 6 accomplishes this purpose. The location of the circle on the R and X diagram is dependent upon the ratio of I_0 to I_R while the radius is dependent only upon I_0. Figure 8 has been drawn for different ratios of I_0 to I_R and is similar to Figure 3 except that the circles do not go through a common point. Figure 9 shows these coils transferred to the R and X diagram. The displacement of the R and X circle with constant diameter to infinity corresponds to the disappearance of the current circle at the origin when I_R is made infinitely large compared to I_0.

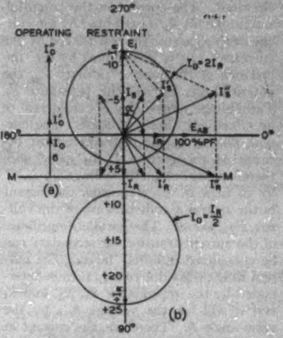

Figure 7. Vector diagram of current and voltage forces acting on the beam

The circuit shown schematically in Figure 6 provides independent adjustment of the diameter of the impedance circle, its location on an axis, and the angle of the axis. The following considerations will bring out the method of accomplishing these adjustments. Consider for the moment that the reactor X_c and the secondary of the reactor X_v are open-

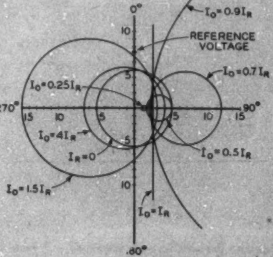

Figure 8. Another way of shifting the current-phase-angle circles to keep impedance circle radius constant

circuited. The current in the potential-restraint coils R_R contributed by the voltage drop R_t across the resistor fed by the current I_A–I_B is determined by the impedance of the reactor primary Z_v, the potential coil R_R, and the resistance R_t. The voltage source E_{AB} is considered to be effectively short-circuited. The current flowing in the restraint circuit because of the current I_A–I_B alone will, therefore, lag behind the voltage $(I_A–I_B)R$ by an angle B. Now consider the current in the restraint coils because of the voltage E_{AB} alone. The parallel impedance of the current-transformer secondary can be considered infinitely large. The current in the relay due to E_{AB} then is determined by the impedances Z_v, R_R, and R_t and it will lag the voltage E_{AB} by the same angle B. Therefore, the current in the restraint coils due to the voltage E_{AB} and the current I_A–I_B will be in phase or 180 degrees out of phase when I_A–I_B is in phase with E_{AB}. Now consider what is necessary to place the characteristic circle on the R and X diagram so that its center is on an axis 60 degrees lagging the R axis and so that the circle passes through the origin. From Figure 7 it is seen that an R and X circle passing through the origin requires a straight-line current locus as shown. Also note that the straight-line locus always occurs perpendicular to the current in the restraint coils contributed by the fault current I_A–I_B when this current is directly opposite to the current in the restraint coils produced by the voltage E_{AB}. These two currents are directly opposite when I_A–I_B is in phase with E_{AB} as was just shown. Therefore, if the voltage across the resistor R_t is caused to lead I_A–I_B by 60 degrees the direct opposition of voltage and current ampere turns in the restraint coils will occur when the fault current I_A–I_B lags E_{AB} by 60 degrees thus placing the R and X circle on the 60-degree axis. The circle, of course, is made to pass through the origin by making the pull of the current I_0 on the operating side of the beam just balance that of the current I_R on the restraint side. This is done merely by regulating the number of turns and core-screw setting on the impedance element and the turns on the transformer primary and the tap on the resistance R_t. For complete flexibility it is desirable to be able to shift the axis along which the characteristic is to be moved to a maximum of 120 degrees. This means that when the fault current I_A–I_B lags the fault voltage E_{AB} by 120 degrees the current and voltage energizations in the restraint coils must be in direct opposition. Since the current in the resistor R_t was advanced in phase a fixed amount of 60 degrees by adding the shunt reactor the only way to obtain 120 degrees is to lag the current in the restraint coils due to the voltage E_{AB} by 60 degrees. This is done by passing a current through the secondary winding of the reactor X_v in phase with E_{AB} because of the resistor R. This adds a component of current almost at 90 degrees to the current already in the primary and, therefore, the phase angle of the current with respect to E_{AB} can be varied over a range of 60 degrees by regulating the amount of the resistance R.

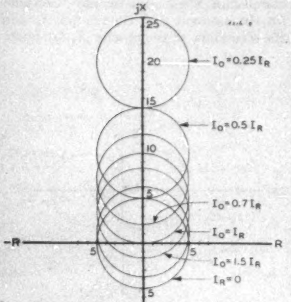

Figure 9. The impedance circles on *R* and *X* co-ordinates resulting from Figure 8

By using a circuit similar to Figure 6 to provide the vector sum of the current and voltage to the contact end of the beam with a restraint proportional to voltage it is possible, by the proper selection of constants and phase shifts, to produce the type of characteristic depicted in Figures 1 and 2. This was covered in a paper before the Institute several years ago.

Course adjustment of the center of the circle is provided by the primary taps on the current transformer while continuous adjustment between taps is provided by a slider on the resistor R_t. The diameter of the circle is controlled by the standard tap and core-screw (*TS*) adjustment on the impedance-element current coil. The axis of the circle is rotated by adjustment of the resistor R_ϕ. All of these adjustments are entirely independent and anyone can be changed without affecting the others.

It becomes evident that a relay with such a flexible characteristic materially aids in the problem of avoiding tripping on load currents and synchronizing currents without jeopardizing the ability to trip faults. With reference to Figure 5, the circle $Z2$ is an example showing how the characteristic can be placed so that the fault ohms L_{FR} can be included in the circle and still have the much shorter synchronizing-surge vector fall outside the tripping zone.

Figure 10 shows the range over which the new relay characteristic can be adjusted. The current-transformer primary and resistor setting R_t are calibrated in ohms (Z_R) and when $Z_R = 0$, the center of the characteristic circle is at the origin or in other words the characteristic is pure impedance without directional discrimination. The radius of the circle is determined by the tap and core-screw setting on the beam-current coil. For any value of the radius the center of the circle can be shifted up the axis by the setting Z_R without changing the radius of the circle. The circle can be shifted in the opposite direction merely by reversing the polarity of the current or voltage. In addition to this the axis on which the circles are shifted can be rotated between 60 degrees and 120 degrees lagging in steps of 12 degrees by adjusting $R\phi$.

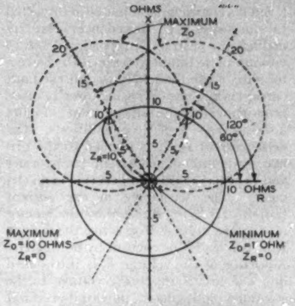

Figure 10. Range of adjustment of impedance characteristic of new relay

The adjusting network is housed in an external case there being three networks in each case so that each distance element of the standard three-zone impedance relay can be adjusted. Furthermore, the relay (type *HZM*) and the external adjusting case are arranged so that the characteristic of either one, two, or three distance elements of the relay can be supplied with the modified characteristic.

Conclusions

1. The utilization of the vector sum of the current and voltage on the operating or restraint side of a beam-type distance relay makes it possible to impart any type of circular characteristic whatsoever to the distance element.

2. The ability to introduce an adjustable amount of phase-angle discrimination into an impedance characteristic means that, once the transmission-line ohm conditions are known, the relay characteristic can be readily molded to fit the conditions best.

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Carbon-Pile Voltage Regulators for Aircraft

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WITH the increase in size of modern military aircraft and the complexities of its equipment, it has been necessary to enlarge greatly its electric system. Up to the time of the beginning of the second World War in Europe, the Tirrell and step-type regulators were used universally to regulate the voltage of aircraft generators, alternators, and motor generator sets. With the coming of lightweight 30-volt 100- and 200-ampere generators, the field current required began to exceed the limitations of the Tirrell regulator. It became apparent that a high-field-current voltage regulator would have to be developed since the appreciable weight saving gained by designing generators with high field currents is so vital to modern aircraft.

This requirement was met by adapting the well-known art of carbon-pile regulation, long-used in railroad lighting equipment, to aircraft. It is the purpose of this paper not to dwell too deeply on the actual design features of the regulator itself, but to acquaint the reader with a general knowledge of its principle of operation, and to discuss its advantages and disadvantages and its numerous applications.

General Construction

Figure 1* shows a sectional view of a conventional present-day carbon-pile regulator, and Figure 2 shows the same regulator not sectionalized. Close examination of Figure 1 discloses that the regulator consists primarily of

(*a*). An ironclad magnetic circuit enclosing a controlling solenoid and an adjustable core.

(*b*). An armature assembly which is mounted on radial leaf springs with a center contact plug insulated from the armature assembly which is the electric connection to the movable end of the carbon pile.

(*c*). The carbon pile consisting of precision-made carbon disks surrounded by and held in place by an insulating tube which in turn is enclosed by a finned housing for dissipating the heat from the carbon pile.

(*d*). An adjustable screw insulated from the rest of the regulator and containing a carbon button. This screw and button acts as the contact for the stationary end of the carbon pile.

Other details of Figure 1 show the regulator proper mounted on a quickly detachable mounting base on which is mounted a fixed resistor and a variable rheostat for raising or lowering the regulated voltage.

Principle of Operation

When the solenoid is unenergized, the armature is pushed against the carbon stack by the springs, thus compressing the carbon pile so that its resistance is at its very minimum. Energizing the solenoid causes the armature to be magnetically attracted to the ironclad magnet case. Magnetic force acts in the opposite direction to the spring force. The compression force exerted on the carbon pile is the difference between the spring force and the magnetic force. It is this varying magnetic force, created by the solenoid, that tends to overcome the spring force and thus vary the pressure on the carbon pile, and hence its resistance. Any change in the resistance of the carbon pile directly effects the output of the machine whose voltage is being regulated, which in turn tends to raise or lower the voltage across the solenoid. Since the regulator is extremely sensitive to any change of *NI* in the solenoid, a balance of magnetic and physical forces takes place in the regulator, and regulation becomes automatic.

Temperature compensation is obtained by the proper selection of the resistance ratio in the coil circuit and by matching a bimetallic spring support to the expansion characteristic of the regulator. Total resistance of the coil circuit consists of copper resistance and an external near-zero-temperature-coefficient resistance connected in series. The bimetal has a slight angle on which the armature springs rest. With an increase in temperature, the bimetallic washer tends to flatten out or the angle decreases. In this way the pressure exerted by the springs lessens. This in turn compensates for the decrease in the magnetic pull resulting from the heating of the copper solenoid coil and other parts affected by temperature. Present-day regulators are temperature-compensated to give satisfactory voltage regulation at ambient temperatures ranging from −50 to +71 degrees centigrade.

Adjustment of the Regulator

Three separate adjustments are provided in the regulator:

1. Coil-circuit resistance or coil-current.
2. Core or air-gap.
3. Carbon-pile compression-screw or spring-tension adjustment.

Adjustment of Coil-Circuit Resistance

Coil-circuit resistance is set by means of the adjustable resistor or rheostat to a value predetermined by the manufacturer. In cases where adjustable rheostats are used, the resistances are so chosen that the contact slider arm will be set in the middle of the rheostat resistance range. This will permit a raising or lowering of the regulated voltage after the regulators have been adjusted properly and put into service. Once the coil-circuit resistance has been set, it should not be changed throughout the remainder of the adjustment procedure since the proper *NI* in the solenoid in this way has been fixed for a given regulated voltage. Also, a proper resistance ratio is established to give good temperature compensation.

Preadjustment of the Core

When the regulator is first assembled at the factory, the core is screwed in until its face is just flush with the face of the magnet case next to the armature assembly. At this point the core is center-punch-marked at the adjustment end, and a corresponding center-punch mark is put on the end plate. In this way, it is possible to line up the two cen-

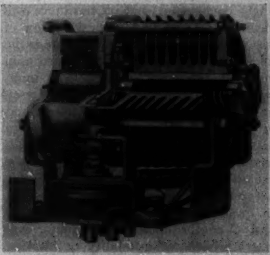

Figure 1. Sectional view of modern carbon-pile regulator

This unit regulates d-c generator output for both single- and parallel-generator systems

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[*] The regulator, as shown in Figures 1, 2, and 4, is of the type disclosed in Newton United States patent 2,268,718 under which the United States Government holds a nonexclusive license for the duration of the war.

Paper 44-184, recommended by the AIEE committee on air transportation for presentation at the AIEE Los Angeles technical meeting, Los Angeles, Calif., August 29–September 1, 1944. Manuscript submitted April 10, 1944; made available for printing July 6, 1944.

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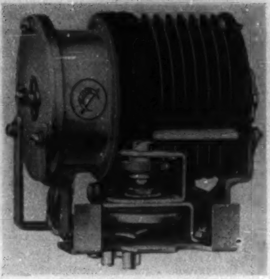


Figure 2. Same unit as shown in Figure 1, except not sectionalized

This carbon-pile voltage regulator is for use in conjunction with single-voltage generators rated from 1.5 to 12 kw

ter-punch marks after the entire regulator has been assembled and thus set the core flush with the magnet case. Correct set position of the core should be such that the core air gap is slightly greater than when set in the flush position.

Adjustment of Pile Compression Screws

With the regulator adjusted as already indicated, the carbon pile should be connected in series with the control circuit of the unit whose voltage is to be regulated and the coil circuit connected across the output. Run the unit so that it would normally produce its rated output if the regulator were properly set. For the purpose of illustration, assume that the carbon-pile compression screw is just turned in to the point at which the contact plug makes contact with the carbon pile. As the pile compression screw is turned in, the entire armature assembly will be moved toward magnet case, increasing the spring tension and decreasing the air gap between the armature and core. Increasing the spring tension will cause the carbon pile to be correspondingly compressed, and its resistance will decrease. Continued inward motion of the pile compression screw will cause the regulated voltage to rise to a maximum point *B*, (Figure 3), decrease to a minimum point *D*, and then increase very rapidly from *D* to *E*. From *A* to *B* and from *E* to *F* (Figure 3) the unit fails to function as a regulator, but acts as a manual carbon-pile rheostat. In the region of the curve from *B* to *E* the unit acts as an automatic carbon-pile rheostat or regulator. However, between *B* and *C* on the curve, the regulator will pass through an instability or hunting stage, becoming less and less violent and finally disappearing as point *C* is approached. It is obvious that no instability in the regulator can be tolerated at any time, as this causes the carbon pile actually to open the circuit, and a destructive arc results which burns the carbon disks, greatly shortening their life and, hence, the useful life of the regulator. Instability in the carbon pile can be detected readily by connecting a sensitive undampened voltmeter, an oscillograph or a set of earphones with at least 1,500 ohms impedance, across the carbon pile, the oscillograph and earphones being the most sensitive. Fluctuation of the carbon-pile voltage will cause the voltmeter needle to fluctuate and will cause the earphones to become very noisy. In the case of the earphones, violent instability will cause an unbearable noise as compared to a quiet hum when stable operation of the regulator is obtained. Best regulating position of the pile screw occurs between *C* and *E*. In general, a setting of the pile compression screw to the left of the minimum point *D* will give a flat or rising voltage characteristic with load as the point set nears the instability region. Likewise, a setting to the right of the minimum point *D* will give a drooping voltage characteristic with load and will be a more stable setting of the regulator. When the approximate correct setting is obtained by the pile compression screw, the regulator voltage probably will be too low. If this is the case, the regulated voltage can be raised by turning the core plug out very slowly, thus increasing the air gap. Regulated voltage is lowered by turning the core plug in, but under no circumstances should the face of the core plug be turned in beyond the flush position. After the core plug is adjusted, the compression-screw adjustment should be rechecked. This procedure should be repeated as many times as necessary until no further adjustment of either the core plug or carbon-pile compression screw is required. All locking screws should be tight. Final setting must be such that the regulator is stable under all possible operating conditions of speed, load, or input voltage, or the useful life of the carbon pile will be short. Much can be written on the regulator adjustment in specific cases, but the aforementioned procedure, in general, applies to all carbon-pile regulators. In actual cases where units are set at the factory, the violent instability region of the curve is smoothed out by lowering the input voltage or by reducing the speed, in the case of generators, to values far below that at which they normally operate. In this way it is possible to adjust the carbon-pile compression screw and follow through the curve to point *C* or *D* without any instability whatever. After the pile compression screw has been adjusted to this point, the speed or voltage may be raised and finer adjustments made without damage to the carbon pile caused by arcing between the carbon disks. All adjustments should be made only by trained personnel and with the proper equipment, as wrong adjustments result in high voltage which is disastrous to most loads.

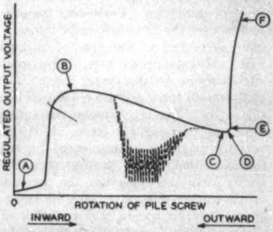


Figure 3. Effect of pile-screw rotation on regulated output voltage

If regulator becomes unstable, voltmeter needle will fluctuate rapidly, and that portion of voltage characteristic will be as shown by dotted lines in diagram. Best regulating position of pile screw is shown at C

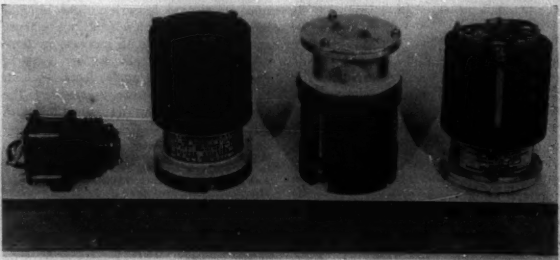


Figure 4. Four different-wattage-size regulators

Left to right, 20, 35, 50, and 75 watts continuous dissipation in the carbon pile. Ratings of all can be safely raised by forced ventilation

Practical Application

At the present time, there are four different-sized regulators in production based upon the maximum continuous wattage dissipation in the carbon pile; namely, 20, 35, 50, and 75 watts (see Figure 4).

All of the preceding can have their ratings increased greatly by forced ventilation. Although all four sizes use the same-diameter carbon disks, the lengths of the carbon piles are different. The smallest or 20-watt regulator weighs $9^1/_4$ ounces and uses a one-half-inch stack of carbon disks; the 35-watt size weighs two pounds, three ounces, and uses $1^1/_2$-inch carbon pile; the 50-watt size weighs $2^1/_2$ pounds (with base shown) and has $1^1/_2$-inch carbon pile; and the 75-watt size weighs two pounds, three ounces, and has a two-inch carbon pile. Carbon piles are available in various different carbon-disk thicknesses and grades. By varying the length, grade, and disk thickness, many different resistance ranges can be obtained. Any of the aforementioned regulators can be made to regulate different given voltages by the selection of the proper solenoid coil.

Carbon-pile regulators have been used in many different applications. For the most part, these applications can be divided into three different classifications, namely:

(*a*). Those regulating the output of engine-driven d-c generators.

(*b*). Those regulating the output of high- and low-frequency engine-driven alternators and motor-alternator sets.

(*c*). Line voltage regulators.

When used to regulate the output of engine-driven d-c generators, the generator is usually a shunt type for use on 12- or 24-volt systems, although there are cases where 115 volts d-c systems are employed as well. In all of the preceding systems, the carbon pile is connected in series with the shunt field of the generator, and the solenoid coil circuit is connected across the output of the generator.

PARALLEL OPERATION

Where generators are operated in parallel as in multiengine airplanes, each generator is made to deliver its share of the load by the use of a load-compensating coil wound as a separate winding on top of the main regulating coil in the regulator. This compensating winding is used in conjunction with a calibrated drop connected between generator negative and ground. In some cases the drop through the series winding is utilized. With the aid of an equalizer bus, if one generator carries more than its share of the load, the voltage drop will be greater in its circuit than the others.

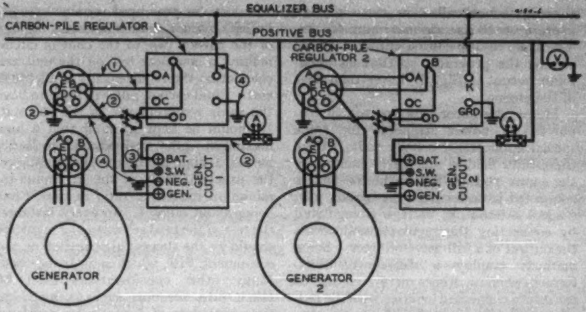


Figure 6. Typical schematic wiring diagram showing installation of *M-2*, O-4, and *P-2* generators with carbon-pile voltage regulator

For parallel generating systems, any number of generators may be connected in the manner depicted in these diagrams, whereas for single systems generator 2 and its associated equipment and connections are omitted from the systems shown. Also to be omitted are the equalizer bus and connections to the *D* and *K* carbon-pile regulator terminals, with use of a single-pole single-throw line switch Caution: Ground (*E*, receptacle) connection of each generator in parallel system shown must provide 0.5 volt drop at full-rated generator current

Disconnect Plug Designation	Generator Circuit
A	*F*+
B	*A*+
C	Vacant
D	*A*−
E	*A*−*F*−

Minimum Brown and Sharpe Cable Size

Generator Type	*M-2*	O-4	*P-2*
Circuit 1	16	16	14
Circuit 2	8	4	0
Circuit 3	18	18	18
Circuit 4	16	16	16

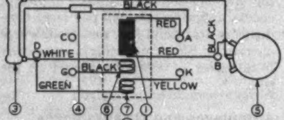


Figure 5. Internal wiring diagram

75-watt carbon-pile regulator with detachable base shown in Figures 1 and 2

1 —Carbon pile
2—Regulator assembly
3—Resistor—tubular
4—Resistor—1,500 ohms
5—Rheostat
6—Coil—shunt
7—Coil—equalizer

A current will flow through its equalizer coil which will lower its voltage and hence its load and, at the same time, boost the voltage and the load in the other generators. In that way it equalizes the loads. Very little change in voltage is necessary to make the generators parallel properly, and practically no change in the bus voltage will be noted.

OTHER APPLICATIONS

Other applications of carbon-pile regulators in conjunction with generators consist of current limiters and wattage limiters. Current limiters limit the output current of the generator to a predetermined value, usually full load plus ten per cent. Its carbon pile is connected in series with voltage-regulator carbon pile, and its coil consists of a few turns of heavy conductor connected in series with the line. When the current reaches the predetermined value, the carbon pile begins to increase its resistance, decreasing terminal voltage and limiting the output current. For the most part current limiters are not used, because modern aircraft generators are designed to carry heavy overloads without damage for the short duration of landings or take-offs.

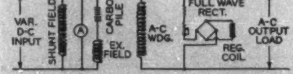


Figure 7. Motor–alternator set and regulator

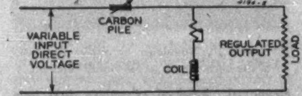


Figure 8. Line-voltage-regulator application

Wattage limiters are much the same as current limiters and work the same way, except that they have an extra potential winding on the same coil as the current winding and are used where generators are driven by auxiliary gasoline engines. The purpose of this type of regulator is to limit output of generator to safe value so that excessive overloads will not stall the engine but will allow the engine to operate up to its safe maximum output. The same results can be obtained by designing the generator so that its maximum output is slightly lower than that of the engine.

Motor–alternator sets are widely employed as power suppliers for some special application, usually radio. Here the exciter field is separately excited by the input voltage to the motor and is connected in series with the carbon pile. Output alternating voltage is regulated by connecting the regulator coil across the output of a full-wave rectifier. Some methods employ a step-down transformer, whereas others merely consist of resistance connected in series with the rectifier. Weight saving in the latter case is a large factor. Carbon-piler regulators have been used as frequency regulators in motor–alternator applications where frequency must be closely regulated. Constant frequency is obtained by regulating the speed of motor.

Line-voltage regulators are used in applications where the line voltage varies with loads, and so forth, as in the case of the electric system of an airplane, and it is desirable to maintain some value of voltage (below the minimum input variation) at a constant value. Such a load, requiring a constant input voltage, might be a filament load of radio tubes or other instruments whose accuracy depends upon its input voltage held constant. In these systems, the carbon pile is connected in series with the load, and the regulating coil circuit is connected across the load.

From what has been said, it is obvious that before selecting a regulator for a particular application, it is necessary to know

(*a*). The regulated voltage plus or minus allowable limits.

(*b*). Maximum continuous carbon-pile wattage dissipation.

(*c*). Maximum and minimum resistance.

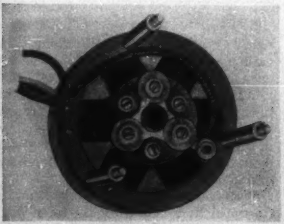
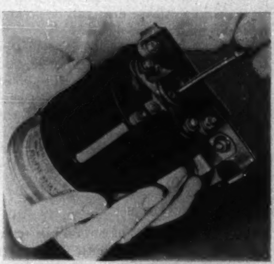


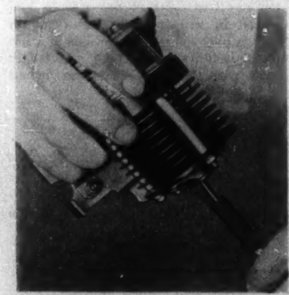
Figure 9. Position of armature assembly and connector lead in magnet case

View shows armature assembly assembled in magnet case. Center portion is carbon button which connects to the moving end of the carbon pile

The last two can be determined readily by employing a manual rheostat in place of the carbon pile in the control circuit making a complete test on the unit and calculating the requirements of the rheostat. Final carbon pile will have to have the same requirements as the rheostat It should be kept in mind that a high maximum pile resistance necessarily means a high minimum pile resistance, the practical limit of the maximum-to-minimum resistance ratio in most cases being about 30 to 1. In every instance, the test of the final selection of a regulator should be the successful operation on the equipment with which it is to be used. Many other considerations must be taken into account, such as matching time constants of a particular regulator with the time constant of the unit with which it is to be regulated. The fact that a regulator will operate satisfactorily with one unit does not necessarily mean that it will operate properly with a unit of a different design but having the same regulating resistance requirements. Reactance voltage, commutation, and time constants all enter into the picture and must be considered. Their effect can be determined only by actual tests.

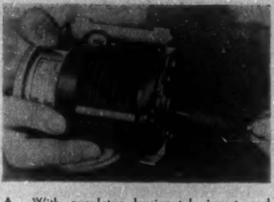


A. Slide a thin rod into the hold in the carbon pile and then . . . .

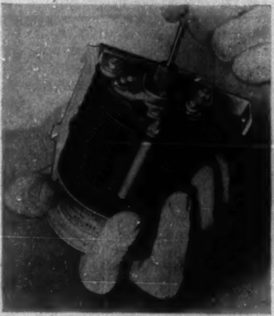


B. Invert the regulator to allow the disks of the carbon pile to fall out on the rod

Figure 10. Removal of carbon disks from carbon-pile regulator



A. With regulator horizontal, insert end of rod carrying disks of carbon pile into the ceramic tube then . . . .



B. Turn regulator up to a vertical position and allow the disks of the carbon pile to fall into the tube. Do not touch the disks with the fingers

Figure 11. Assembly of carbon-pile disks into ceramic tube of regulator

Although the regulator is rugged in its construction and can stand much abuse, it must be remembered that it is primarily an instrument. The armature assembly has mass and is not counterbalanced (as all attempts to do so have resulted in a sluggish regulator) and is, therefore, sensitive to shocks and violent vibration. Proper consideration given to its mounting and location with respect to violent shocks and vibration will pay large dividends in its useful life, and trouble-free operation. Shock mounting in areas away from center of vibration should be the rule.

Advantages and Disadvantages

Certain disadvantages or shortcomings are experienced with the present carbon-pile regulator, as follows:

1. The regulator has three adjustments; this number will be reduced in future designs for simplification purposes.

2. Repairs and adjustments should be made by trained personnel at bases where reliable test equipment and accurate instruments are on hand.

Higher-Voltage D-C Aircraft Electric Systems

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Synopsis: Many of the advantages of 115-volt d-c aircraft electric power are presented with frequent comparisons to 24-volt direct current and 208/120-volt 400-cycle three-phase alternating current. System reliability is emphasized. Weight comparisons favorable to 115-volt direct current are shown for power generation, distribution, and load components. Problems concerning the use of the two high-voltage systems are discussed and compared. The popular belief that 115-volt d-c switching and commutation is unsatisfactory at high altitudes is refuted. It is concluded that 115-volt direct current is the lightest and most practical of the proposed electric systems for both medium and large airplanes.

CURRENT discussion of large aircraft electric systems has been carried on mainly in terms of commercial power distribution experience. There has been a tendency in such discussions to make blanket statements concerning the superiority of a-c systems without due consideration of direct current or aircraft-design requirements. Extrapolation of commercial practice and experience out of the field of its origin into aircraft design remains to be proved. The airplane presents a very special electrical design problem. Only when a-c systems are applied to actual aircraft designs, and after a careful competitive check made of both a-c and d-c results, are valid conclusions possible. A few facts learned from one such study on an actual airplane will be presented in this paper.

Weight and Safety

Reliability and weight are the principal determinants of aircraft electrical design. These factors are far more important in aircraft than in other fields of electrical engineering. One of the large air-transport companies has estimated the value of weight saved over a five-year operating period at $600 per pound per airplane. In today's larger airplanes electricity has acquired many new responsibilities, such as engine controls, flight-surface controls, landing flaps, and landing gear, many of which are essential to flight. Thus, the design of the electric system may be literally a life or death matter.

The reliability of an aircraft electric system is a function of weight and engineering experience. Currently proposed higher-voltage a-c and d-c systems for large aircraft may be made equally reliable. However, the weight and accumulated experience necessary to achieve this result will be greater for the a-c than for the d-c system. Some of the proposed a-c systems have secured a favorable weight by sacrificing present standards of reliability. Any higher-voltage system which does not provide at least the same degree of safety as present low-voltage d-c systems employing main-engine-driven parallel-operated generators is not comparable and is not really suitable for aircraft application. The 115-volt d-c system herein discussed is intended to exceed considerably existing standards of reliability in order to assume the new electrical responsibilities. Basic requisites of any aircraft electric system includes such factors as:

1. Maximum system stability under high fault loads.
2. Optimum fault burn-clear characteristics.
3. Uninterrupted full-load capacity during momentary fault conditions.
4. Continuous uninterrupted full-load capacity in the event of failure of one main power source.

System Types

Three principal electric systems are at present receiving extensive design consideration:

1. The established standard 24-volt d-c system.
2. The 115-volt d-c system, which is the subject of this discussion.
3. The 208/120-volt 400-cycle three-phase a-c system which recently has received considerable attention.

If as much study, research, and development had been directed toward higher-voltage d-c aircraft power as has been applied in the past two years to a-c aircraft power, it is believed that the future standard would be the higher-voltage direct current.

On any given airplane a small amount of 400-cycle alternating current or 24-volt direct current may be required, but this does not constitute a significant factor in the design of the main power generation and distribution system.

Functional Breakdown of the Electric System

For the purposes of this discussion the aircraft electric system is functionally separated into three general divisions: (1) distribution, (2) generation, and (3) the load component parts.

The weight of the load component parts of an aircraft electric system is the least effected by the voltage or type of system employed. Thus for a given airplane the total weight of the motors, lights, switches, and miscellaneous equipment may be substantially unchanged for 24-volt direct current, 115-volt direct current, or 208/120-volt 400-cycle a-c designs. While small differences in the weight efficiencies of certain items on the different systems will be apparent, the over-all total weight differences are not likely to be decisive.

The weight of the generative portions of the aircraft electric system will be lighter for 115-volt than for 24-volt direct current, but both will be substantially lighter than comparable 400-cycle a-c designs.

The most important weight differences appear in the distribution system. Large

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3. A minimum resistance is ever present and varies with the maximum resistance with the obvious disadvantage that it never can reach zero.

Advantages of the carbon-pile regulator are as follows:

1. The carbon-pile regulator is capable of regulating high currents.
2. The carbon-pile regulator does not contain any movable contacts to get out of order. These were always a source of trouble due to short life, causing radio interference, susceptibility to high altitudes, and a constant maintenance problem. When properly adjusted, the carbon pile gives a smooth change in resistance.
3. There are practically no moving parts to wear out or get out of order. The only parts requiring replacement are the carbon pile and contact plugs. With the proper replacement of these parts and readjustment, the regulator is as good as new.

Few people can realize what an important part in our war effort this amazing instrument performs. Its true value probably will not be fully known until after the war, when many of its present-day applications will be converted to peacetime use to make commercial aviation safer.

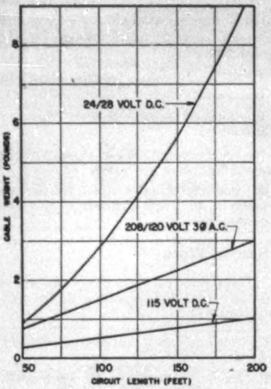

Figure 1. Aircraft cable weight for transmitting one-fourth horsepower

savings in weight with respect to low-voltage direct current are effected by both of the high-voltage systems; however, the 115-volt d-c distribution system is by far the lightest.

Degree of Electrification

Weight analyses for the three electric systems is difficult because of the large variation possible in the degree of electrification in airplanes of the same general type and size. The weight differences between the three systems vary greatly depending on whether or not heavy load items, such as landing gear, landing flaps, flight-control boosters, engine and ventilation controls are electrically operated. Electric-system designs for the same airplane may have a three-to-one variation in weight due to the degree of electrification.

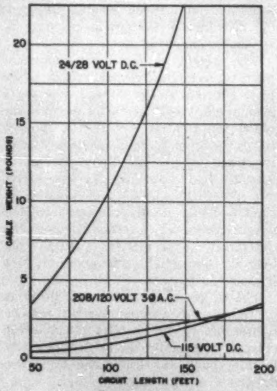

Figure 2. Aircraft cable weight for transmitting one horsepower

In present 24-volt airplanes the large variation in degree of electrification is because, in many designs, there is no decisive weight advantage for either electricity or hydraulics. However, with higher voltage the weight advantage of electric operation may be large because of the saving in wiring weight, particularly on the large airplanes. On such airplanes the wire weight to an item at 24 volts frequently exceeds the weight of the item itself.

Power Distribution

WEIGHT SAVINGS OF 88 PER CENT

The weight for transmitting one horsepower 100 feet over a single-wire-grounded aircraft system with a five per cent allowable voltage drop, or 50 feet over a two-wire circuit, amounts to ten pounds with 24-volt direct current but amounts to less than one pound with 115-volt direct current. Figures 1, 2, and 3 show the relation between weight, distance, and horsepower for the various systems. On large airplanes much of the heaviest wiring, that from main engine-driven generators located in the wings, propeller feathering pumps, empennage turret guns, rudder and elevator boosters, landing flaps, and landing gear can be reduced to 12 per cent or less of its present weight by changing from 24 to 115 volts.

CONDUCTOR WEIGHT

Voltage is the principal factor determining the weight of the distribution system. Metallic conductor weight in a d-c system varies with respect to voltage:

1. Inversely as the square of the voltage for those conductors determined by voltage drop.
2. Inversely as the voltage for conductors determined by current carrying capacity.

Reduction in cable weight can be carried only as far as the minimum conductor gauge (at present number 22). Thus, a change from 24 to 115 volts can result in a weight reduction inversely proportional to the square of the voltage for wires originally number 10 gauge or larger. Smaller wires, determined by voltage drop, will be reduced to the minimum size before a 16 to 1 weight reduction can occur. Similarly, a reduction to one fourth of the 24-volt-system weight, for conductors determined by current carrying capacity, will be limited to number 16 gauge and larger.

CABLE INSULATION WEIGHT

The insulation of some aircraft cables appears to be adequate for any of the higher-voltage systems now under consideration. In terms of voltage change the conductor insulation weight varies:

1. Inversely as the voltage for wires determined by voltage drop.
2. Inversely as the square root of the voltage for wires determined by current carrying capacity. Cable insulation weight varies with wire size from ten per cent of the total cable weight for number 00 to 50 per cent for number 22.

CONDUIT WEIGHT

Voltage, as it affects cable size, is also a major factor in determining the weight of many other accessories, such as, cable terminals, splices, connectors, junction boxes, and conduit. In terms of voltage, conduit weight will:

1. Vary inversely as the voltage for cables, the sizes of which are determined by voltage drop.
2. Vary inversely as the square root of voltage change for cables determined by current carrying capacity.
3. Not vary for instrument and other cables already containing minimum-size conductors.

EXAMPLES OF 24-VOLT WEIGHT INEFFICIENCY

A weight analysis of the 24-volt electric system of a 50,000-pound gross-weight four-engine transport indicated that:

1. Seventy-five per cent of the total conductor weight was determined by the five per cent voltage-drop limitation.
2. Five per cent of the total conductor weight was determined by current capacity limitations.
3. Twenty per cent of the total conductor weight was not affected by voltage change.
4. Seventy per cent of the total cable weight was estimated to be metallic conductor, and 30 per cent to be insulation.

Thus the weight efficiency of the 24-volt d-c system is poor even on present airplanes. In the example just cited only seven per cent of the conductor weight of cables larger than minimum size was operating at maximum current carrying capacity. Even two-engine airplanes in the 20,000-pound gross-weight class will have less than 50 per cent of the conductor weight of cables above minimum size operating at maximum capacity. For good weight efficiency it is desirable that aircraft electric-system voltage be high enough, so that substantially all of the wiring is either operating at maximum current carrying capacity or is reduced to the minimum allowable size.

In designing 24-volt electric systems for some of the really large airplanes now under construction with gross weights of several hundred thousand pounds, it is found that less than one per cent of the conductor weight of cables above minimum size would be operating at maximum current carrying capacity. It is believed that the magnitude of the weight penalty paid for use of present 24-volt aircraft electric systems is not generally appreciated. On the four-engine transport airplane previously mentioned it was estimated that more than 100 pounds of wiring alone could have been saved by designing for 115-volt direct current.

The wiring-system weight of one large eight-engine airplane was calculated for two different designs. With a maximum degree of electrification the electric cable and bus weight totaled 2,190 pounds for 24-volt and 906 pounds for 115-volt direct current, a saving of 1,284 pounds. With a minimum degree of electrification the electric conductors had a total weight of 1,100 pounds for 24 volts and 600 pounds for 115 volts, a saving of 500 pounds.

Comparison of A-C and D-C Cable Weight

Actual design figures indicate that three-phase a-c power-distribution systems will weigh considerably more than comparable d-c systems for the following reasons:

1. Currently proposed a-c systems require three conductors for each three-phase circuit. Only a single conductor is needed in commonly used grounded return d-c systems. Application of three-phase a-c power to circuits already reduced to minimum size in 115-volt d-c designs will result in a three-fold increase in weight for the wiring of these circuits. Increasing the present electric system voltage approximately four times, from 24 volts to 115 volts, will reduce to minimum size more than 50 per cent of the total cable weight of even our largest airplanes of several hundred thousand pounds gross weight. While a considerable portion of this minimum-size wiring will be for single-phase lighting and instrument loads, an increase in total cable weight of 40 per cent or more can be expected from this cause when three-phase four-wire grounded-neutral a-c systems are compared with equivalent grounded-return d-c systems.

2. Power factors from 40 to 70 per cent are expected for 400-cycle motors and other inductive items. It is estimated that additional weight for power-factor correction or increased cable size will raise the total cable weight from 5 to 10 per cent.

3. Connectors, terminals, conduit, and other devices incidental to the power-distribution system, of course, will be lighter for a single-wire grounded-return d-c system than for a three-wire three-phase grounded-neutral a-c system. Roughly, the total estimated weight of these sundries for one specific design was 20 per cent more for alternating current than for 115-volt direct current.

On airplanes over a hundred thousand pounds gross weight, the weight of three-phase grounded-neutral a-c power distribution probably will exceed by 50 per cent the total weight of a single-wire grounded-return d-c system, voltages being the same. At present even the 208÷120-volt three-phase wiring weight is considerably heavier than that for the 115-d-c equivalent.

In the future, when airplane designs require several hundred kilowatts of electric power, it may prove desirable to use 230-volt d-c or perhaps 230/115-volt d-c systems. While potentials higher than 115 volts have only a small weight advantage at the present time, they do introduce insulation and switching problems.

This study indicates that 115-volt direct current offers weight savings even for small airplanes of 25,000 pounds or less gross weight. This is not true of three-phase alternating current, since the relative weight disadvantage of three-phase a-c power distribution increases rapidly with decrease of airplane size.

Comparison of A-C and D-C Distribution-System Reliability

Safety of any system or device is largely a function of its simplicity. The single-wire d-c aircraft distribution system certainly achieves the optimum of simplicity. Statistically, it can be shown that protective devices, fuses, circuit breakers, and relay systems can be carried beyond the point of optimum reliability. Every protective device added to a circuit or system carries with it the failure liabilities inherent in that device.

On main distribution lines and feeders it is desirable to provide multiple fault protection within the lines. This practice has for more than a decade been used in industrial and municipal power-distribution systems. Figure 4 illustrates a simplified three-wire multiple fault-protected feeder system of familiar design. It will be noted that a fault at point X would be fed by parallel fuses A, B, C, and D in series with the faulted line's single fuses E and F. It is obvious that fuses E and F, each fed by two units in parallel, would blow, clearing the line of this fault. This type of circuit protection is of doubtful value in low-voltage systems, since contact-resistance and slight-resistance inequalities of the feeder branches will cause unequal current distribution in the feeder lines sufficient to upset the effective fault discrimination of the circuit. At higher voltages these same factors have a negligible effect on the protective action and fault discrimination of the circuit. Not only does this provide increased safety and reliability for all main feeders, busses, and essential circuits, but also, with a single-wire 115-volt d-c design little of the essential protection of this system is lost by the added complexity. The case is very different when the same type of three-wire multiple fault protection is attempted in a three-phase four-wire a-c distribution system. In such circuits the number of protective devices would be multiplied by three over those required for direct current. It is doubtful whether a three times increase in complication and circuit-protector inherent liability would result in an overall safety gain. Many essential circuits having minimum-sized wire can be protected justifiably in a single-wire d-c system.

Both safety and cost of airplane operation are affected favorably by the simplicity of the single-wire d-c distribution system. Present service and design and manufacturing personnel would have to be trained especially for the safe operation of a-c aircraft systems. The application of 115-volt d-c aircraft power would involve no essentially new techniques nor operational hazards.

Circuit-Interrupting Equipment

Design Fundamentals

Interrupting equipment for 115-volt direct current can be lighter than existing 24-volt direct current or 208-volt three-phase alternating current.

The design parameters of 24-volt direct current, 115-volt direct current, and 208-volt alternating current for aircraft switches differ radically. The widespread belief that switching of 115-volt direct current is difficult results from unsatisfactory performance of certain available 24-volt equipment when tested at 115 volts. Assertions that adequate 115-volt d-c switches will compare in size and weight with the conventional industrial counterparts are incorrect. It is also improper to expect a-c equipment to perform satisfactorily on direct current.

A low-travel snap-action-type switch, having a 0.070-inch contact gap and rated ten amperes at 24 volts direct current, interrupts less than one ampere at 115 volts direct current. Also, an *AN*-3023 aircraft toggle switch rated ten amperes at 24 volts interrupts less than one ampere at 115 volts. Actually, three amperes in a 115-volt d-c inductive circuit will support an arc across a one-eighth-inch gap. Such performance has discouraged the adoption of 115-volt d-c aircraft power. This attitude is not justified, since well-known design principles can and have been applied to solve this problem. These switch-design principles include:

1. Multiple break.
2. Deion grid.
3. Magnetic arc suppression.
4. Solid insulation arc barrier.
5. Liquid arc barrier.
6. Hermetical sealing.
   (*a*). Pressurized gases.
   (*b*). Vacuum.

Sufficient empirical data have been compiled which definitely disprove many statements made concerning switch inadequacy for the higher-voltage direct current.

115-Volt D-C Switches Are Available

For immediate application the Cutler Hammer Company offers a line of 110-

220 d-c "tool-handle" switches, which have 5-, 10-, and 20-ampere industrial ratings. Actual tests have indicated that these switches may be used at approximately half of their commercial rating for altitudes up to 40,000 feet when connected as single-pole double-break units. These switches are designed to interpose a solid insulation barrier between the contacts, which effectively suppresses the arc. For equivalent power interruption these switches are lighter than the conventional 24-volt line.

A double-pole toggle-switch type *AN*-3023 has six times the single-pole capacity when the two poles are connected in series for a double break.

Low-travel snap-action-type switches are available with magnetic-arc-suppression provisions and are rated ten amperes at 115 volts direct current for operation up to 40,000 feet. One such switch was tested with and without the magnet from sea level to 50,000 feet with various contact spacings. The capacity of the switch was increased by 400 per cent with the proper use of the arc suppressing magnet. It is possible to use a small magnet for arc-extinguishing purposes for as little as one tenth of an ounce additional weight per switch. Within the aircraft industry it has been stated that a magnetic field is ineffective as an arc suppressing agent at low pressures, and that arc reversal occurs at certain altitudes. A careful check was made under altitude simulated conditions up to 50,000 feet. All of the mentioned altitude irregularities of devices incorporating magnetic arc suppression were experienced, but on investigation proved to be due to inadequate insulation and spacing. The tests performed indicate that accepted theories of arc behavior in magnetic fields are correct.

The 24-volt type *AN*-3022 aircraft toggle switch may be used safely for three-fourths of an ampere at 115-volts direct current for altitudes up to 50,000 feet. At 115 volts this is 90 watts, and the switch should satisfy more than 40 per cent of the total switch requirements.

A study of airplane switching requirements indicates that 90 per cent of the loads on a large airplane will require a three-ampere switch for 115 volts and a 15-ampere switch for 24 volts.

THREE-PHASE A-C SWITCHING

The three-pole switch for three-phase alternating current is inherently heavier and more complicated than the single-pole switch commonly used in d-c circuits.

SWITCH DEVELOPMENTS

The preceding discussion concerns conventional switch design. An important electric function, such as aircraft switching, demands equipment designed specifically for the purpose. A great deal of effort has been expended in the last two years in the attempt to interest aircraft-accessory manufacturers in the design and production of hermetically sealed switch gear. Obviously, a switch with contacts sealed in an enclosure with hydrogen or some other suitable gas under pressure would not be deleteriously affected by altitude variation, humidity, moisture, ice, dust, foreign matter, contact oxidation, and ionization. A small hermetically sealed switch manufactured by the Western Electric Company for use in submarine cables was tested with encouraging results. This switch, measuring one quarter of an inch in diameter and two inches in length, had magnetic leaf-type contacts enclosed in a hydrogen atmosphere. The switch was actuated by an external magnet and satisfactorily interrupted more than one ampere at 115-volts direct current in a circuit having an L to R ratio of 0.020. This same switch is adaptable to electromagnetic operation as a relay. At the present time higher-capacity 15-ampere sealed switches and relays with double-throw contact arrangements are being developed by several companies.

RELAYS

In general the remarks concerning switches apply as well to relays. A line of relays originally designed for three-phase 208-volt alternating current recently has been given 115-volt d-c ratings. They are available in three sizes up to 5-kw continuous duty and 20-kw rupturing capacity. For d-c use the ratings are based on series connection of the three poles, thus constituting a single-pole relay having a six-break mechanism.

Another relay originally designed for 24-volt direct current has been adapted to 115-volt d-c service by the addition of an 0.05-ounce Alnico magnet for each pair of contacts. Tests indicated that this relay would interrupt safely ten amperes at 30,000 feet and six amperes at 50,000 feet.

It is interesting to note that an industrial-type relay rated 100 amperes at 115 volts direct current weighed 25 pounds, while a comparable aircraft relay designed for the same voltage weighed only 1.75 pounds. Assertions that higher voltage will result in extremely heavy switch gear probably are founded on the consideration of such commercial equipment. It should be noted that the aircraft units were designed to much more severe requirements for an ambient-temperature range of $+158$ to -85 degrees Fahrenheit, and for vibration and acceleration loads in excess of ten grams. In fact, this 115-volt relay weighs less than a 24-volt relay having a comparable power rating. The weight of a high-current 24-volt contact mechanism is inherently heavier than that for the lower-current 115-volt power equivalent. Specifically, the weight of current carrying components is substantially greater than that of arc suppression mechanisms.

CIRCUIT-PROTECTION DEVICES

Circuit breakers incorporating electromagnetic arc extinction have proved satisfactory for 115-volt aircraft circuit protection. Design investigations are being conducted to adapt these circuit breakers to overload protection and circuit-switching service. The General Electric Company has a series of 208-volt a-c 115-volt d-c fuses which may be preferred to circuit breakers.

Lighting

FILAMENT CHARACTERISTICS

In the design of aircraft 24-volt lamps for low-wattage sizes, the filament has been drawn to minimum diameter and in some cases has been etched to reduce further the cross-section area. When it is suggested that lamps be designed for 115-volt service, it is argued that filaments are already too fragile. In many instances these low-wattage lamps give insufficient light and would be better replaced with brighter lamps. To get this higher output the filament need only be increased in length, the diameter being the same. In increasing the length it is assumed that the filament support will be of a sturdy vibration-resistant character. The filament-diameter worry exists only with the low-wattage low-lumen output lamps. The filament of a 50-watt 115-volt lamp could be the same diameter as the widely used 15 candle power 24-volt aircraft lamp. The increased efficiency of the electric generating system plus the reduced power loss in high-voltage distribution systems, since much of the cable already will be of minimum gauge, permits the use of higher-wattage lamps. The problem with 115-volt landing lamps is with the filament support rather than with the filament diameter. Several 115-volt lamps with different-type filament supports have been tested under simulated aircraft-vibration conditions. Certain lamps which have especially designed vibration-resistant filament supports operated for at least 250 hours when vibrated over a range of 100 to 3,600 cycles per minute with a total excursion of 0.031 inch. Therefore, it is concluded that satisfactory lamps are available in 20-, 40-, and 50-watt sizes.

LAMP LIFE AFFECTED BY MOUNTING

Where lamp life is important the lamp-assembly mounting should be designed carefully. An actual lamp installation was mounted on a platform and vibrated with a 0.031-inch total excursion. The filament movement was measured to be from 0.062 to 0.25, depending on the resonant characteristics of the mounting. This is mentioned because lamp failure is frequently the result of faulty installation rather than lamp design. Shock mounting of lamp assemblies may be desirable to increase the life of certain essential lamps such as landing lights.

A-C Lamps

Filament resonance, sometimes encountered with a-c lighting and especially with 400-cycle power, does not exist with direct current. In order that phase balance may be maintained during lamp on and off conditions it is necessary that high-wattage lamps for alternating current have special three-phase filament structures. For smaller lamps it is general practice to distribute lamp loads among the three phases with resultant wiring and phase-balance complication.

Present Supply Problems Temporary

The aircraft electrical designer is having difficulty in obtaining a wide variety of adequate 115-volt lamps. This is probably due to the high production demands on lamp manufacturers because of war necessity.

Radio

It is frequently stated that alternating current is ideal for radio power, while direct current, because of voltage-conversion difficulties, is not. This is a fallacy, because fundamentally radio equipment requires direct current which is normally obtained from alternating current by rectification and filtering. The aircraft radio designer seldom chooses tubes for desired d-c potentials but instead resorts to dynamotors, inverters, and transformers. This procedure is neither desirable nor necessary. At present 24-volt direct current is being used successfully as the only operating potential in some aircraft radio receivers and in at least one transmitter, except for the power stage. Equipment so designed has proved the equal of previous designs in all respects and superior as regards to voltage breakdown of capacitors and insulation. A d-c potential of 115 volts can be used with greater facility and even better results. Beam-power amplifiers represent a step in the direction of low-voltage transmitting tubes.

Motors

General Considerations

On large airplanes having a gross weight of over 100,000 pounds, 150 or more motors may be installed. On one well-known airplane at least 125 motors are used for armament alone. Although motors represent as little as 20 per cent of the airplane electrical equipment, they may constitute as much as 75 per cent of the entire electric load.

Motor weight is a function of the performance required. In general the weight of d-c motors for high-starting and peak-torque service will be less than for alternating current. For example, a peak output of eight horsepower will require a two- or three-horsepower d-c motor or about a four-horsepower a-c motor. On the eight-engine airplane previously referred to, about 70 per cent of the motors were estimated to be of the high-torque intermittent-duty type.

Motor Efficiency

Efficiency of intermittent-duty equipment is not important, since power loss due to inefficiency of such equipment is negligible. The weight of intermittent-duty equipment can be reduced by designing for lower efficiency, and such weight saving usually will more than justify such design. Only where the peak horsepower requirements of a motor approach the peak generation output of the aircraft electric system is efficiency of intermittent-duty motors a critical factor.

The efficiency of a 115-volt d-c motor will be higher than that of a comparable 24-volt motor due to lower brush losses. For a given power output a 115-volt d-c motor will be lighter than an equivalent low-voltage motor by virtue of smaller commutator and brushes.

Brushes

A great deal has been said recently concerning brush wear. Many conflicting theories have been expounded for the erratic performance of brushes at high altitude. It is not within the scope of this discussion to review or conjecture on the suppositions now prevalent. Excessive brush wear at high altitudes usually has been evident where current densities have approached 200 amperes per square inch.

As a result of current military need for improved 24-volt brush life, research and development have progressed to the point where 1,000 hours' service has been assured for motors and generators from sea level to 40,000 feet. The same techniques will be applied to 115-volt d-c rotating equipment with even longer brush life. In the near future brush maintenance on d-c motors may be almost negligible in view of the development of new brush materials.

Explosion-Resistant Motor Requirements

Identical explosion-resistant requirements exist for both a-c and d-c, high or low voltage motors. The assumption that brushless motors are explosion proof is unjustified since they are equally susceptible to:

1. Faulty workmanship.
2. Insulation breakdown.
3. Stalled rotor overheat.

Any of these faults may result in ignition of explosive fuel mixtures.

Power Generation

Altitude Effects

Military demands have increased the altitude ceiling of airplanes to 40,000 feet or more. This does not mean that such high-altitude flight will prevail after the war. Airplanes may fly as high as 25,000 feet in order to avoid storm conditions, but it is probable that commercial transport and cargo planes seldom will be flown above 20,000 feet. Direct current is generally conceded as being satisfactory for electric power up to 25,000 feet, but it is claimed that alternating current is the only answer for very high altitudes. Lengthy technical papers have been published in which higher-voltage d-c system considerations were dismissed with a few perfunctory remarks to the effect that higher-voltage d-c power is not suitable for high-altitude service. Experience and test results indicate that direct current is not only adequate, but may be the best type of very high-altitude aircraft power. Satisfactory performance of d-c inverters, radio dynamotors operating at 1,000 volts or more, and electric fuel pumps on airplanes being flown at extremely high altitudes substantiate this statement. The a-c systems now under consideration employ a d-c generator for alternator excitation. This practice seems inconsistent if d-c generation is not considered entirely reliable at high altitudes. Objection to the use of direct current at high altitudes has come from the improper use of equipment of doubtful low-altitude design at 30,000 to 40,000 feet. The failure of marginally designed equipment, such as generators having brush current densities of over 200 amperes per square inch, does not constitute valid reason for objection to the

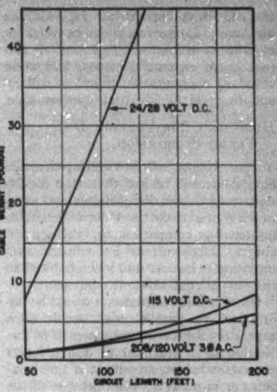

Figure 3. Aircraft cable weight for transmitting three horsepower

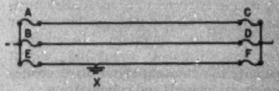

Figure 4. Three-wire feeder

use of direct current for high-altitude airplanes. Conservative design which incorporates maximum compensation, normal brush current densities, and other standard commutation practices will result in good high-altitude performance.

HIGH- AND LOW-VOLTAGE D-C WEIGHT COMPARISON

The weight of 115-volt generation equipment will be less than that for 24-volt design. Smaller brushes and commutator area make the 115-volt generator lighter than comparable low-voltage machines. High-current low-voltage commutation is heavier and less reliable than the low-current 115-volt equivalent.

On very large airplanes it would be impractical to parallel ten 24-volt 6-kw generators for a required power of 60 kw. Neither is it practical to design larger generators having an output of 1,000 amperes or more at 30 volts because of the commutation difficulties and unwarranted commutator weight. Present 115-volt generators are at least 15 per cent lighter than equivalent 24-volt units. Appreciable weight savings are obtained for the 115-volt d-c voltage-regulation equipment. On the same eight-engine airplane it was estimated that the high-voltage generation system was lighter than the low-voltage generation system by approximately 150 pounds.

A-C AND D-C WEIGHT COMPARISON

There should be little difference in weight between comparable a-c and d-c generators when the necessary alternator field excitation is included. Comparison of constant-speed alternators with variable-speed d-c generators is confusing and wrong. The important weight difference between a-c and d-c machines comes from generator drive requirements. A variable-speed d-c generator is very much lighter than the combination of an alternator and its associated constant-speed drive mechanism. The speed of airplane propulsion engines is varied from approximately 1,000 to 3,000 revolutions per minute. To maintain constant frequency within practical limits it is necessary to interpose an automatic constant-speed transmission to drive the alternator from a main engine. The weight of a variable-speed d-c generator is somewhat more than that of a constant-speed generator, but this weight difference is small compared with the weight of a constant-speed drive mechanism required for constant-frequency alternating current. This constant-speed transmission device is proving to be a serious design problem. It is expected that the weight of these constant-speed drives can be reduced approximately to two pounds per horsepower for the 60-kw alternator. In addition to an estimated 150-pound weight penalty the constant-speed drive creates unknown hazards and complication.

The difficulty with a-c design does not end with securing relatively constant frequency. There is the additional problem of providing means for paralleling the alternators. The relative position of paralleled 400-cycle alternator rotors must be the same with only a very small angular displacement permissible. Equipment to accomplish such synchronization is usually complex.

A method of avoiding parallel operation of alternators is to divide the airplane loads between two independent alternators with automatic means for transferring the loads from an alternator, which may fail, to the remaining one. The divided load system will require more elaborate distribution wiring and bus installations, more extensive load protective measures, and complicated transfer equipment. The load transfer system to be practical must be automatic and sufficiently rapid, so that there is no appreciable interruption of service. There should be no danger of stalling motors, of de-energizing relays, nor of interrupting solenoid-operated equipment. A load transfer system is usually troublesome, and its design is technically difficult. The reliability of such a system is questionable and cannot be compared with the inherent safety and simplicity of parallel-operated d-c generators.

For alternating current, a divided load system on large aircraft will require two alternators driven at a constant speed, each of which must have sufficient capacity to carry the entire load. The size and weight of the alternator and constant-speed-drive combination does not permit direct mounting on a main engine. A remote accessory drive shaft and gear box would be required, and it is to be remembered that space aft of the engine is at a premium. A proposed remote drive for each 30-kva alternator was estimated at 50 pounds, a weight penalty which a-c systems can ill afford. If it is assumed that a large airplane will require 60 kva, the generation system will have 120 kva available in two alternators. For the same requirements six main engine-driven 15-kw d-c generators can be used with much greater reliability. The six d-c generators having 90-kw output will be considerably lighter than the 120-kva alternator installation.

AUXILIARY ENGINE POWER

Auxiliary engine-driven aircraft alternators have been operated successfully in parallel, and this system constitutes a standby until the proposed systems are proved satisfactory. The reason for present consideration of other a-c power systems is the weight inefficiency of constant-speed auxiliary electric drives which will be explained.

Auxiliary power may be the answer to many electrical design problems, but the efficient use of such power cannot be obtained where constant speed operation of the auxiliary engine is required. This is because the average electric load for long flights may be less than 30 per cent of the peak demand. The fuel consumption may be reduced only 36 per cent for a 70 per cent reduction of load. Another cause of high auxiliary engine fuel consumption is the fact that, to compensate for the loss of power at altitude and to supply customary short-time overload capacity, the auxiliary engine size must be increased considerably over the nominal requirements. For a required 50-kw peak load at 20,000 feet, a 175-horsepower auxiliary engine is needed. Of this 175 horsepower an average of only 15 kw will be used in flight, yet 64 per cent of the full-load gasoline consumption will be required. An estimate of fuel required by two 50-kw auxiliary engines operated at constant speed throughout a 25-hour flight was 600 pounds more than for main engine-driven generators.

Fuel consumption of auxiliary engines can be reduced materially by governing engine speed in conformity with load requirements. This may be accomplished by providing a load-sensitive speed governor for the auxiliary engine, driving a variable-speed d-c generator. A two-to-one speed regulation of the auxiliary from peak load to average load is required to show favorable weight comparison with present main engine-driven designs. Load governing of the auxiliary between wider limits would result in additional weight savings. It is obvious that the advantages of variable-speed load governed auxiliary power-plant design are practical only with the use of direct current.

Where auxiliary power plants are installed for engine starting and for on-the-water operation, it is desirable that they also be used in flight. Because of vibration complications, service troubles, and crowded accessory compartments, it is desirable to remove the generators from the engine. By centralizing the power supply a material saving can be made in wiring and bus weight.

BATTERY POWER

In the electric-system design there may be some need for limited amounts of stored energy which can be supplied by storage batteries. Depending on the capacity required, it may prove lighter to install a small auxiliary power plant in place of the storage battery. In either case the stored energy can be conveniently a part of a d-c system. The battery constitutes an extremely inefficient source of electric power, and its use will be avoided wherever possible.

Conclusion

For large aircraft, regardless of altitude service, 115-volt direct current is more practical than either 24-volt direct current or 208/120-volt 400-cycle three-phase alternating current. The 115-volt system is considerably lighter than either the 24-volt d-c or the a-c system.

Application of Electronics to Aircraft Flight Control

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PROBABLY no other control problem offers so many opportunities for the advantageous application of electronics as the design of equipment for automatic flight control. The type *C*-1 Autopilot used in the Army's heavy bombardment aircraft is a typical example of equipment in which the use of electronic devices has produced a flexible well-co-ordinated control system from what was formerly an unwieldy electromechanical machine.

Inherent Design Problems

To appreciate fully the problems in the design of an automatic pilot it is first necessary to understand the fundamental principles of flight control.

The control of an airplane can be resolved about three perpendicular axes, as illustrated in Figure 1. When the position of the airplane is such that its roll and pitch axes are horizontal and its turn axis is vertical, it will fly "straight and level"; but, whenever the airplane is made to rotate about one or more of these three axes by movement of its ailerons, elevators, or rudder, its flight path is changed. The ailerons, elevators, and rudder therefore are called the *control surfaces* of the airplane. It is important to remember that the position of these control surfaces does *not* determine the airplane's position, but rather the rate of the airplane's rotation about its corresponding axes. This characteristic of airplane flight control makes it necessary for the pilot to perform double operations in executing simple maneuvers.

As an example, consider the operation of the ailerons in making a left turn. First the airplane is made to roll to the left by displacing the ailerons—the left aileron upward, the right aileron downward. As the airplane reaches the desired angle of bank, the ailerons again are streamlined to stop further rotation, and the airplane remains in the bank until the desired turn has been made. The ailerons are then displaced in the opposite direction for the time required to return the airplane to level flight. Rudder and elevator controls are operated in a similar manner, except that the elevator and, to a lesser degree, the rudder are not completely streamlined during the maneuver.

Another complication in aircraft flight control arises from the fact that, in most turning maneuvers, the ailerons, rudder, and elevator all must be operated simultaneously to produce a proper turn. Too much rudder will cause a skid; too much aileron, a slip; and too little up-elevator will permit the plane to lose altitude in the turn. Therefore, any automatic pilot must accurately proportion the movement of each control to fit a complex pattern and, at the same time, co-ordinate the movement of all controls to maintain them in proper relationship with one another.

In addition, it is highly desirable for an automatic pilot to be so universally adaptable that identical units from the production line will operate effectively when installed in different types of airplanes. Since different types of aircraft have widely different flight characteristics, and since these same characteristics are influenced materially by speed, loading, altitude, and weather conditions, an automatic pilot, to be universally adaptable, must have provisions for easily adjusting its operating characteristics to match those of the airplane.

The use of vacuum-tube circuits not only simplifies the solution of these problems but also makes possible a degree of sensitivity and accuracy not obtainable by any other means. These features are of basic importance in an automatic pilot designed for high-altitude precision bombing.

Description of Autopilot Equipment

The primary function of an automatic pilot is to fly the airplane straight and level. It must operate the controls quickly and smoothly in response to any deviations from an established attitude and heading.

In the *C*-1 Autopilot the vertical flight

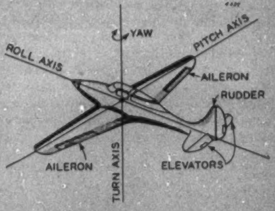

Figure 1. Flight-control axes of an airplane

Figure 2. Vertical-flight gyroscope

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Although a few problems exist with the use of either high-voltage system, those for direct current are not difficult, while a satisfactory solution for the many a-c problems is questionable.

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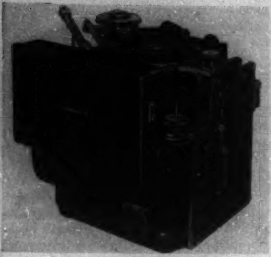
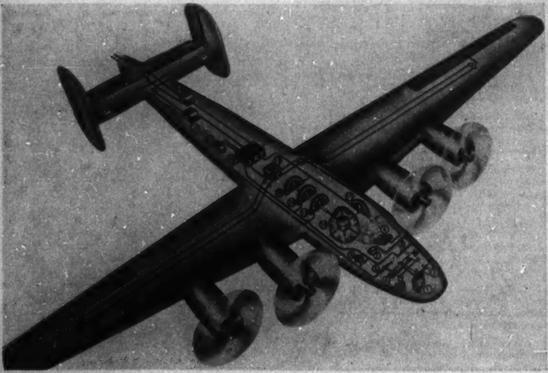


Figure 3. Directional stabilizer

gyroscope is employed as a reference for roll-axis and pitch-axis deviations (see Figure 2). Its outer case is mounted firmly to the structure of the airplane, but its rotor is universally mounted within the case so that the gyroscope's automatic erection system can maintain the rotor axis vertical, regardless of the position of the airplane. Any tilting of the gyroscope case with respect to the rotor is measured electrically by four potentiometers within the unit. These potentiometers have their windings fastened to the case and their sliders held in position by the gyro rotor. The resulting electrical signals activate the system to restore the aircraft to level flight. Since pitch-axis deviations are corrected by elevator control alone, only one potentiometer is located on the pitch axes. However, three potentiometers are located on the roll axis of the unit, as all three controls (aileron, rudder, and elevator) are employed to correct roll-axis deviations.

Directional or turn-axis control is derived from the directional stabilizer (Figure 3), which contains a horizontal gyroscope. The rotor of the horizontal gyroscope tends to remain fixed in space, unaffected by turning movements of the airplane. Here again, potentiometers with their windings fastened to the outer case and their sliders held in position by the gyro-rotor assembly are used to measure electrically any course deviation of the airplane. Two potentiometers are used, one for rudder control, the other for aileron control.

Figure 4. Schematic diagram of the Autopilot system



Thus, in the two gyro-controlled units a total of six potentiometers are employed as the sensing elements of the system. The individual units are identified in Figure 4 by the numbers 1 and 10, and the potentiometers by the numbers 5, 6, 11, 12, 13, and 14. A seventh potentiometer (number 2 in the illustration) is an auxiliary part of the Autopilot, used to energize a direction indicator (7) on the pilot's instrument panel.

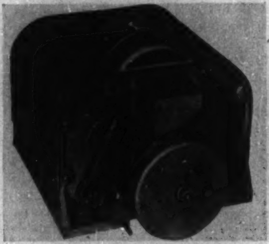


Figure 5. One of the three servo units

No less important in the operation of the Autopilot are the motorized servo units which do the actual work of moving the control surfaces (see Figure 5). Three such units are required, one each for ailerons, rudder, and elevator. (In Figure 4 these units are located at 15, 18, and 19.) Each servo unit contains an electric motor which drives a cable drum through a differential gear train. Two solenoid-operated clutches control the direction of cable-drum rotation; one clutch turns the drum in one direction, the other in the opposite direction. An automatic brake mechanism effectively holds the cable drum stationary when neither of the two clutch solenoids is energized. Cables wound on the servo-cable drum are attached to the main control cables which move the control surfaces.

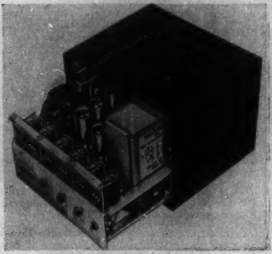
Each servo unit also contains a balancing potentiometer with its winding fastened to the servo-unit frame and its slider attached to the cable drum. The purpose of these balancing potentiometers will be revealed later in the explanations of the controlling bridge circuits.

The seven-tube amplifier (Figure 6) is the electronic brain of the Autopilot. It contains three distinct channels, one for each of the three flight-control axes (item 16 of Figure 4). Each channel contains two tubes—a 7*F*7 double-amplifier stage, a 7*N*7 discriminator stage, and two relays which operate the clutch solenoids of the related servo unit. For the moment, each channel of the amplifier may be thought of as a motor controller which analyzes the amount and direction of control-surface movement called for by the sensing potentiometers in the bridge circuit and which then operates the corresponding servo unit accordingly.

Manual control and adjustment of the system are provided by the Autopilot control panel (Figure 7). This unit centralizes at the pilot's fingertips the various switches and control knobs by which he engages the system, aligns the Autopilot with a selected attitude and heading, adjusts the sensitivity of the Autopilot, regulates the amount of control surface applied to correct a given deviation, and co-ordinates interrelated control functions. A "turn control" is included by which the human pilot can execute turns and evasive tactics without disengaging the Autopilot.

Power for the d-c components of the system is supplied by the airplane's batteries. A rotary inverter provides the

Figure 6. Autopilot amplifier



19-volt, 105-cycle a-c power required. The entire Autopilot equipment, with all seven motors, 14 solenoids, and seven-tube amplifiers energized and operating, draws only 7.6 amperes from the 26-volt d-c batteries. Normal voltage and frequency variations have relatively little effect on the performance of this Autopilot system.

The Bridge Circuits

If the gyroscopes are thought of as the *eyes* of the Autopilot, the servo units as the *muscles*, and the amplifier as the *brain*, then the bridge circuits might be likened to the nervous system by which all these components are interconnected.

There are three distinct bridge circuits of the resistance type in the system—one each for aileron, rudder, and elevator control. Each bridge circuit provides the input to a specific amplifier channel which, in turn, operates one of the servo units.

Figure 8 is a schematic diagram of the aileron bridge circuit, which has been simplified for the purpose of explanation. The aileron-control potentiometer, shown at the top of the diagram, is located in the vertical-flight gyroscope and is sensitive to roll-axis deviations of the airplane. The balancing potentiometer at the bottom of the diagram is actuated by the cable drum on the aileron servo unit. Both potentiometers are energized by a common transformer in the amplifier.

If we apply Kirchhoff's law to this circuit,

$$e_1 - e_2 = E \tag{1}$$

As long as e_1 and e_2 are equal, E is zero and no signal is applied to the amplifier, and the servo unit remains stationary; but, when the control potentiometer is displaced, e_1 and e_2 are no longer equal and a signal E is impressed on the amplifier. Depending on the phase characteristic of E, the amplifier then causes the servo unit to drive the balancing potentiometer in the proper direction to make e_2 equal to e_1 and thereby reduce E to zero. In effect, the potentiometers, amplifier, and servo unit comprise a self-balancing bridge circuit in which the balancing potentiometer produces a corresponding movement of the cable drum to drive the control surfaces of the airplane. This is the fundamental operating principle of the *C*-1 Autopilot.

Figure 7. Autopilot control panel

Actually, of course, the circuit is complicated somewhat by the necessary inclusion of additional control potentiometers, as shown in the diagram of the aileron bridge circuit (Figure 9). The principle of operation, however, is the same; and the same type of equation applies:

$$E = e_1 \frac{r_1}{R_1} + e_2 \frac{r_2}{R_2} + (e_3 - e_4 - e_5) \tag{2}$$

In this equation, E is the voltage output of the entire bridge circuit, and e_5 is the voltage applied by the balancing potentiometer.

When E is zero, the circuit is balanced and the system is at rest; but, whenever any potentiometer in the system is moved, the voltage (E) is effective on the amplifier and causes the servo unit to drive in the proper direction to adjust e_5 to such a value that E again will be zero.

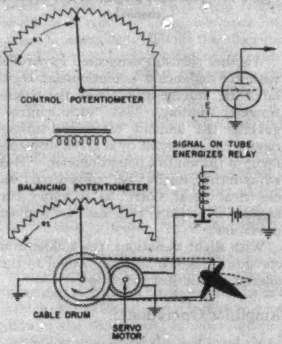


Figure 8. Simplified bridge circuit

In normal flight, e_1 and e_2 are zero, because the potentiometer wipers are at their center taps, as indicated by the dotted lines on the diagram. The other values are such that E is zero and the circuit is in balance. When the airplane is blown off course, the banking potentiometer on the directional gyroscope is displaced. This produces a signal, e_2, which causes operation of the servo unit and a corresponding increase in the value of e_5 to rebalance the circuit. Movement of the ailerons immediately causes a bank and consequently a reduction of e_3 as the vertical-flight gyroscope is tilted. This reduction of e_3 offsets the effect of e_2 and results in reversal of the servo unit to streamline the ailerons when the degree of bank becomes proportional to the directional deviation.

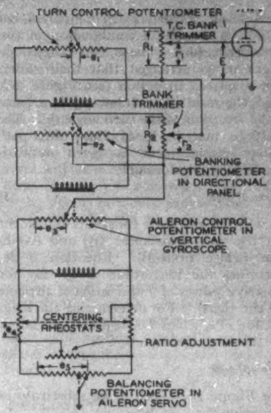

TURN CONTROL POTENTIOMETER
T.C. BANK TRIMMER
R_1
r_1
e_1
E
BANK TRIMMER
R_2
r_2
e_2
BANKING POTENTIOMETER IN DIRECTIONAL PANEL
e_3
AILERON CONTROL POTENTIOMETER IN VERTICAL GYROSCOPE
CENTERING RHEOSTATS
e_4
RATIO ADJUSTMENT
e_5
BALANCING POTENTIOMETER IN AILERON SERVO

Figure 9. Complete bridge circuit

Meanwhile the airplane is turning back toward its original heading, reducing the value of e_2. Since e_2 is now less than the change of e_3, the circuit is again unbalanced, and the servo unit applies opposite aileron to reduce the degree of bank. As the airplane levels off, the value of e_3 increases toward its original level-flight value. Because both e_2 and e_3 now are approaching their normal values, the servo unit again reverses to restore e_5 to its normal value. Thus, the ailerons reach their trim position in level flight as the original heading of the airplane is restored.

Note that the turn-control trimmer and the bank trimmer are provided to regulate the effect of the turn-control and banking potentiometers by applying the fractional factors r_1/R_1 and r_2/R_2.

Among the advantages of using potentiometers in a-c bridges are:

1. Simple isolation of voltage sources.
2. Controllable amplification of small signals.
3. Linearity of control characteristics.
4. Nondistortion of wave forms.
5. Full 180-degree phase change at balance point.

A linear relationship exists between the deviation in attitude of the airplane, as measured by the potentiometers on the gyroscope and the displacement of the control surfaces. The voltage output of a bridge potentiometer can be considered as practically proportional to the displacement of the sliders, because of the high impedance of the amplifier input circuit when compared to that of the bridge circuit. The one exception to this linearity is the banking potentiometer on the directional gyroscope, which is represented by a single potentiometer with linear characteristics in Figure 9, but which is actually a combination of two potentiometers. These two potentiometers are so arranged that their sliders are displaced in unison (see Figure 10), and the resulting voltage (e_2) is a parabolic function of the turn angle (θ). The voltage magnitude of this "dual-pot" slider displacement is given approximately by the formula

$$e_2=a\theta^2+b\theta \tag{3}$$

in which a and b are constants dependent on the fixed resistances (r_3) and (r_4) in the bridge network. Equation 3 determines the magnitude of e_2 only for positive values of θ (deviation of airplane to the right). For determining the magnitude of e_2 when θ is negative (deviation to the left), the equation is

$$e_2=a\theta^2-b\theta$$

See Figure 15. Then if we substitute in equation 2,

$$E=e_1\frac{r_1}{R_1}+(a\theta^2+b\theta)\frac{r_2}{R_2}+(e_3-e_4-e_5) \tag{4}$$

Employment of the resistance bridge with electronic motor control permits the substitution of simple and easily accessible electrical adjustments in place of more difficult mechanical adjustments. As an analogy, imagine how much easier it would be to adjust the carburetor, tappets, and distributor of your automobile if it could be done by turning knobs as you do in selecting a station on your radio. In the electronic Autopilot this ease of adjustment is attained simply by connecting rheostats (located on the control panel) to the bridge circuit as shown in Figure 9. The two centering rheostats are mounted on a common shaft connected in series with the balancing potentiometer, as shown. When the resistance of one is increased, the resistance of the other is decreased. This arrangement permits the pilot to shift the electrical center of the balancing potentiometer in either direction to align the circuit with the mechanical trim position of the control surfaces. These centering potentiometers thus can be likened to "electrical trim tabs."

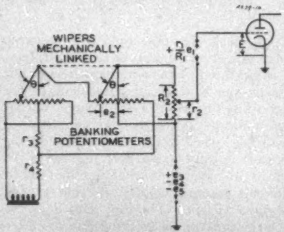


Figure 10. Partial circuit showing dual potentiometer arrangement

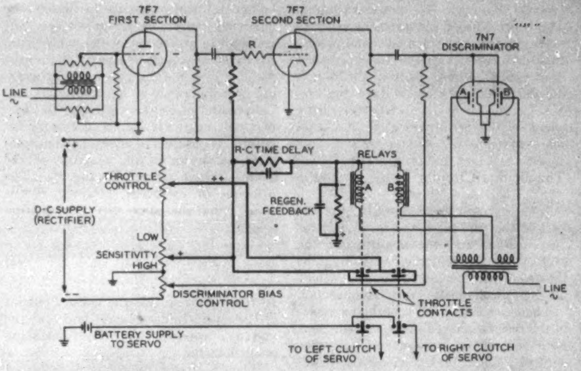


Figure 11. Schematic diagram of one amplifier channel

Another rheostat connected in parallel with the balancing potentiometer regulates the voltage drop across the potentiometer winding. This "ratio control" governs the amount of control-surface movement produced by a given displacement of the control potentiometer. This adjustment might be considered an electrical "gear shift," as it varies the airplane's "speed of response" for a given deviation.

With slight variations, the rudder and elevator bridge circuits are similar to the aileron circuit just described.

Amplifier Operation

So far, the amplifier has been described merely as a phase-sensitive motor controller which responds to any unbalance of the bridge circuits by closing the proper relay, thereby producing the required servo-unit operation. How the amplifier works and how it introduces special control characteristics are revealed by the schematic diagram of one of its three channels (see Figure 11).

Note that only two tubes are used in each channel, one 7*F*7 and one 7*N*7. The first section of the 7*F*7 tube is used strictly as an amplifier, with no d-c bias applied to the grid. The second section, however, is employed in a unique way as a control tube, with three separate controlling voltages applied to its grid.

Attention is called, first, to the resistance *R* which is connected in series with the grid of the second section. It is apparent that any voltage impressed on the left-hand end of this resistor is proportioned between the resistor and the internal grid resistance. When the applied voltage is negative, no current will flow between cathode and grid, as the grid resistance becomes practically infinite. Therefore, the entire applied voltage is impressed on the grid. However, when the applied voltage is positive, current flows between cathode and grid, because the grid resistance becomes very low in comparison with the resistance of *R*, and hence only a small fraction of the applied voltage actually is impressed on the grid. Thus, negative signal voltages applied to the resistor produce sharp plate-current fluctuations, whereas positive voltages do not cause any appreciable change in plate current (see Figure 12). In effect, this resistor lowers the saturation point of the tube's characteristic curve and produces a very flat curve beyond that point.

It is apparent from the resulting characteristic curve (Figure 12) that the *sensitivity* of the tube can be controlled by applying a variable positive bias. Thus, when the tube is biased at point *A* (high sensitivity) a small signal will produce

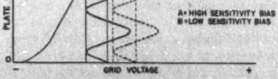


Figure 12. Characteristic curve of the control tube, illustrating the effect of changing sensitivity bias

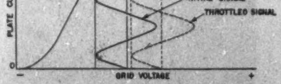


Figure 13. Characteristic curve of the control tube, illustrating the effect of throttling bias

plate-current variations. But when the tube is biased at point *B* (low sensitivity), the same signal is ineffective, and a larger signal is required to produce plate-current fluctuations. This sensitivity bias is knob-controlled by a potentiometer (Figure 11) located on the Autopilot control panel.

The sensitivity bias is applied continuously, but, under certain conditions of operation, two additional bias voltages are applied to this same tube section. One is a negative feed-back voltage which has the effect of increasing the tube's sensitivity whenever either half of the 7*N*7 discriminator tube is passing current. The other is a throttle voltage, of higher positive value than the sensitivity bias, which is applied to the tube whenever the throttle contact on either of the two relays is closed. The effect of this throttle voltage is to increase the bias on the grid to point *C* (Figure 13), thus blocking any signals which are not sufficiently large to overcome this increased bias. (Referring to Figure 13, note that a signal which produces plate-current fluctuations when the tube is biased at *B* is ineffective when the bias is changed to *C*.) A resistance–capacitance time-delay circuit serves to maintain the throttle bias on the tube for a short time after the relay contacts reopen.

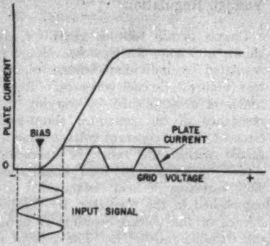


Figure 14. Characteristic curve of the discriminator tube

The purpose of the feed-back circuit is to insure that the weakest signal capable of overriding the bias of the control tube will be amplified sufficiently to drive the discriminator tube, and thus produce positive nonchattering operation of the relay.

The discriminator tube, which employs alternating voltage on the plates, is biased negatively to the cutoff point. This bias is sufficiently large to prevent any current flow through either half of the tube when no signal is applied to the grid (see Figure 14).

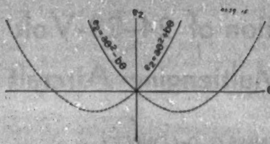


Figure 15. Output voltage of dual banking potentiometers versus directional deviation of airplane

It is apparent from the diagram (Figure 11) that since the two plates of the discriminator tube are connected to opposite ends of similar transformer windings, the voltage of one plate is always 180 degrees out of phase with the voltage of the other plate. When the signal applied to the interconnected grids is positive, the electrons will flow from the cathode to whichever plate is positive at the same instant. Since each plate circuit contains a relay coil, the corresponding relay is energized. Thus the discriminator tube operates one relay or the other, depending on the phase relationship between the input signal and the alternating plate voltage supply. In effect, this discriminator is a grid-controlled rectifier similar to a thyratron.

As illustrated by the diagram, individual relays control the operation of separate clutches in the servo unit and thus control the direction and amount of servo-unit rotation or control-surface movement.

Now, let us follow a signal from the bridge circuit all the way through the amplifier circuit. This signal will be one which calls for counterclockwise rotation of the servo unit, as would be produced by energizing the left-hand clutch solenoid. That is, the signal is in phase with the voltage at plate *A* of the discriminator tube. The signal is first amplified and reversed in phase by the first section of the 7*F*7; then it is applied to the grid of the second section where it produces plate-current pulsations during the negative half of the cycle. Since the signal phase again is reversed in the second section, these plate-current pulsations apply a positive half-wave signal to the discriminator tube which is in phase with plate *A* of that tube. The resulting current flow energizes relay *A*, which in turn energizes the left-hand clutch solenoid of the servo unit.

At first the signal on the control-stage grid of the 7*F*7 is sufficiently large to override the additional positive bias introduced by the closure of the throttle contacts on the relay. However, as the servo unit drives the balancing potentiometer toward the balance point, the signal gradually is reduced until its negative peaks no longer exceed the throttle bias. When this occurs, the signal to the discriminator tube is interrupted, causing the relay to drop out for about one-eighth second while the time-delay capacitor discharges.

When the capacitor is discharged, the throttle bias is lost and the signal to the discriminator is restored. The relay closes again, but only momentarily, because the throttle bias is thereby reapplied. The result is that, as the servo unit approaches the balance point, it operates intermittently with a "pecking" action which prevents overshooting or "overcontrol" of the airplane. The rate of correction, which is fast for large deviations, will diminish gradually as the airplane approaches a stabilized-flight attitude.

Summary

In automatic flight control, as in other control problems, the limit of flight accuracy through mechanical and electrical controlling methods seems to have been reached. Only by the application of electronic-control principles has it been possible to set new standards and to achieve full flexibility of control with completely automatic operation of all control functions. Furthermore, these advantages have been obtained with actual reductions in the total weight of equipment.

Since the use of an automatic pilot for precision bombing imposes complex and rigid requirements which are not encountered in commercial aeronautics, it can be confidently stated that the continued development of electronic controls will produce a small lightweight, foolproof automatic pilot for peacetime use. Because of the precision bombardment Autopilot, the control industry is certainly richer in knowledge and experience, and will be better able to cope with the new problems of peacetime industry.

Although the connection now seems obscure, some of the applications in the development of the type *C*-1 Autopilot were taken from a cabin-temperature-control system. In a similar way, the principles employed in both systems have been found useful in the automatic regulation of engine power. And so, in a seemingly endless chain, each new development brings to light the solution of another problem. Who knows what application these same developments will find in enlarging the prosperity, health, and happiness of a peaceful world?

Paralleling and Regulation of 24-28-Volt D-C Generators in Multiengine Aircraft

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Synopsis: This paper presents the findings of a rather extensive program of testing electric systems in aircraft. Only the system which is presently being used in most United States Army Air Forces multiengine aircraft and a proposed system, which has been designed as the result of the tests, are described. All intermediate steps are not discussed since it is felt that the effects of these steps have been reflected in the proposed system. The proposed system differs from the present system primarily in that it employs separate networks for power feed and distribution instead of a common bus. Several desirable features of such a system are pointed out. Equipment is discussed in a general manner since the aircraft designer is concerned with the design of equipment only insofar as it affects the operation of the system. However, quite often the aircraft designer is in a position to make suggestions for improvement of the equipment.

MOST large United States Army Air Forces aircraft of the present day employ the type of electric system that is outlined by the Army Air Forces specification 32300, "Installation of Electrical Equipment, General Specification for." This is a 24-28-volt d-c single-wire grounded negative system in which the nominal rating of the batteries is 24 volts and the engine-driven generators deliver power at 28 volts. All power is fed into and taken from a common bus which interconnects all wiring of the airplane as shown in Figure 1.

Four main-engine-driven generators and one or more batteries supply power to the airplane. An auxiliary power plant, gasoline-engine driven, is available for ground operation, take-off, and landing. A bus exists across the airplane from number 1 to number 4 nacelles and from the nose of the airplane to the tail. Each generator feeds into the bus at its respective nacelle through a reverse-current relay. The batteries feed into the bus through a relay which is operated by the pilot. External power may be supplied to the airplane while on the ground through an external power plug. It can be seen from this that normally there are five sources of power feeding into the bus: the four generators and the batteries. It is evident that electrical resistance must exist between each and every feeder point. With current flowing the voltage drop, or differential voltage, between inboard and outboard nacelles may be as much as one-quarter volt. To overcome this differential, the outboard generators must generate a higher voltage than the inboards if equal load distribution is to be expected.

Generator Relay

The generator is connected to the bus by a relay (see Figure 2) which closes only when the generator is delivering voltage higher than a predetermined value (approximately 26 volts). A small "pilot" relay *V* which determines the "cut-in" voltage operates a set of contacts *T* in the main winding. The generator switch which is controlled by the pilot completes the circuit. In addition, the relay opens in the event that generator voltage drops below bus voltage and causes reverse current to flow through the generator. Permanent magnets are used to polarize the pilot relay so that current passing through coil *R* in the normal direction strengthens the field but reverse current induces magnetomotive force in opposition. As the induced magnetomotive force approaches the strength of the permanent field the attraction on the relay armature is nullified and the contacts open. It is this function that gives the relay its common designation as a reverse-current relay.

A new relay has recently been developed to replace the reverse-current relay. It is referred to as a differential-voltage relay and gets its name from its operating characteristic. Provided the generator is delivering voltage higher than the bus voltage by a minimum value (between 0.25 and 0.50 volt) the relay will close and put the generator in service. In this case, the "pilot" relay operates on the differential in voltage between the generator and bus. There is a series current coil, similar to that in the reverse-current relay, which performs the same functions. These are:

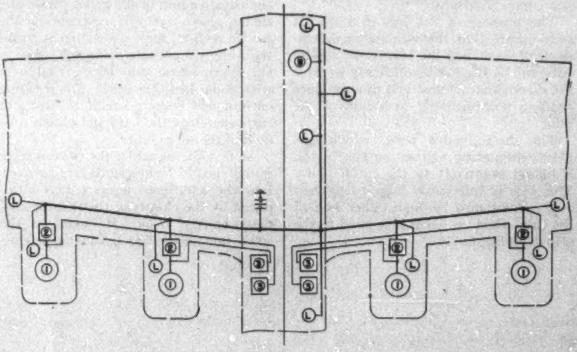
1. To keep the pilot contacts closed after the main relay closes, at which time the differential between generated and bus voltage no longer exists.
2. To open the relay when a reverse current of sufficient magnitude flows into the generator.

Voltage Regulation

Circuit details for one generator are shown in Figure 3. System voltage is regulated by individual voltage regulators (Figure 2) for each generator. Regulation is accomplished by varying the resistance of the generator shunt-field circuit so that a constant voltage is maintained within the range of operating speeds regardless of load conditions. This variation of field resistance is accomplished in the regulator, by the influence of the voltage winding *N* upon the variable resistor *L*. The generator field current is taken through the resistor; variation in resistance causes varia-

Figure 1. Schematic wiring diagram, typical four-engine airplane

1—Engine-driven generator
2—Reverse-current relay
3—Voltage regulator
9—Auxiliary power plant
L—Load center



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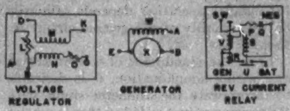


Figure 2. Schematic wiring diagrams, typical equipment

A—Generator field terminal
B—Generator positive terminal
D—Equalizing-coil terminal
E—Generator negative terminal
G—Ground (negative) terminal
K—Equalizing-coil terminal
L—Variable resistor (field)
M—Equalizing winding
N—Voltage winding
O—Variable resistor (adjusting)
P—Fixed resistor (current-limiting)
Q—Rectifier
R—Series current coil
S—Main relay winding
T—Pilot relay contacts
U—Main relay contacts
V—Pilot relay winding
W—Generator field windings
X—Generator armature
BAT—Bus connecting terminal
SW—Switch terminal
NEG—Negative terminal
GEN—Generator positive connecting terminal

tion in generator field current, thus maintaining a constant voltage under all conditions of speed and load within rated limits.

The variable resistance *O* is connected in series with the solenoid regulating coil so that a greater or lesser portion of the line voltage is imposed on the regulating coil, thus increasing or lowering the regulating voltage. Normally, this is the only adjustment made on the regulator.

The regulator is connected to the individual system at the generator side of the reverse-current relay, and is centrally located in the fuselage of the airplane for accessibility in flight. This results in another voltage differential in the system, in addition to the voltage drops in the busses because the connection between the reverse-current relay and the regulator is a different length for each generator. In considering the functions of this connection it is necessary to analyze the currents which it must carry. They are:

1. The current necessary to energize the regulator.
2. Field current for the generator.
3. Operating current for the voltmeter.
4. Operating current for main winding of the reverse-current relay.

It follows that a connector intended to energize the regulator is performing three additional functions. All of these currents are reasonably constant except the generator field current which may vary from approximately three to ten amperes. As a basis for comparison it may be assumed that the length of this connector is approximately 30 feet from the outboard nacelle to the regulator and 20 feet from the inboard nacelle to the regulator. A simple calculation will show that under identical conditions the voltage drop across this connector would be 50 per cent higher for the outboard nacelle than for the inboard. The effect of this condition upon the generator, as shown in Figure 4, is that the generated voltage exceeds the regulator setting by a value equal to the drop across the line. In some cases this value may be as high as one volt. This analysis shows that under all conditions the outboard generators will be delivering a different voltage than the inboards, if all regulators are adjusted to the same value. Hence, a natural tendency for poor load division is introduced.

Load Division

Voltage regulators are provided with an equalizing circuit, which tends to make all generators deliver equal current. The equalizing circuit provides for equal distribution through the influence of winding *M* on winding *N* and the resultant effect on resistor *L*, as shown in Figure 2, by lowering the voltage of the generators which are tending to deliver high current and conversely raising the voltage of those generators which are delivering comparatively low current. In this respect, if one generator is out of service and the equalizing circuit is left intact, it will endeavor to lower the voltage of the others. At the same time the regulator will tend to raise the voltage of the "off" generator so that it will take more load. Since the one generator is inoperative, as far as the system is concerned, its voltage is zero. It is impossible to lower the voltage of the other machines to such a value. However, the equalizing circuit will exert its full energy to accomplish this. The result is a drop in voltage of the generators in service. The magnitude of this voltage drop is dependent upon the number of generators in the system, the generators out of the service, and the total load on the system. At any time a generator is out of service the respective reverse-current relay is open whether the pilot's control switch indicates "on" or "off." For instance, if the switch is "on" but the generator speed drops to a point where it can not carry the load its reverse-current relay will open and remain open until the speed rises again. Thus, it becomes desirable to devise some method whereby the equalizing circuit will be opened automatically. The simplest method for accomplishing this is to add auxiliary contacts to the reverse-current relay. This would require practically no change in the airplane wiring since the equalizing connection from the generator to the regulator usually passes close to the relay.

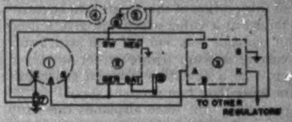


Figure 3. Schematic wiring diagram, one generator circuit

1—Generator
2—Reverse-current relay
3—Voltage regulator
4—Ammeter
5—Voltmeter
6—Generator switch (control)
7—Ammeter shunt
8—Positive bus

Another solution to the problem employs a double-pole single-throw toggle switch rather than a single-pole single-throw as the generator switch. The extra pole is used to open the equalizing circuit at the same time the generator switch is opened. This accomplishes the desired results provided the generator is intentionally put out of service or the operator knows the generator is inoperative, and opens the switch. The objections to this solution are that it is not automatic and additional resistance is introduced by the additional wiring which is required to extend the circuit to the point at which the switch is located.

Energy for the equalizing circuit is obtained from a voltage drop across a portion of the generator circuit, which is specifically the connection between airplane structure (ground) and the *E* (negative) terminal of the generator, as shown in Figure 3. In accordance with Air Corps Specification 32,300 the resistance

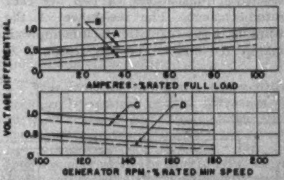


Figure 4. Differential in generated voltage and regulator setting due to resistance of connections between the generator and voltage regulator for a typical four-engine aircraft

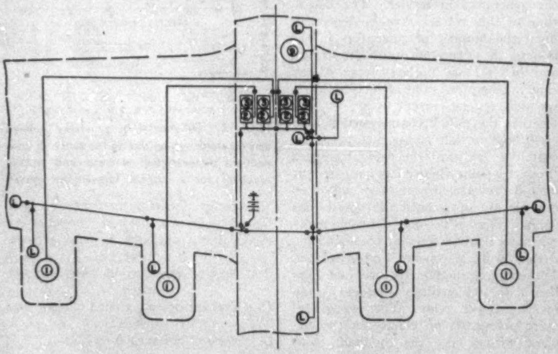
Solid curves—Generator on outboard engine
Dashed curves—Generator on inboard engine
A—Generator operating at rated minimum speed
B—Generator operating at rated maximum speed
C—Generator delivering rated full-load current
D—Generator operating at no load

of this portion of the circuit is to be maintained at a value which will give a drop of one-half volt ±5 per cent with rated current flowing. However, for any given airplane a deviation between generators of more than two per cent should not be exceeded. On some airplanes this drop is accomplished through the use of a given length of copper conductor, the resistance of which may vary widely with temperature. Consequently, the voltage drop may vary with ambient temperature and load conditions. In some airplanes this condition is accentuated by the ground circuit for the inboard generators being located in the wheel well. The conductors which comprise the circuit are exposed to outside air temperatures, which may differ by as much as 200 degrees Fahrenheit from the ambient temperature in the outboard nacelles. The ammeter shunt is usually located in this circuit and with the necessary connectors contributes to the required resistance. A loose connection anywhere in the circuit may cause erratic results as to the voltage drop under given conditions, since contact resistance varies widely when passing current. In this type of installation, all such connections must be controlled. Furthermore, it is important that the ground connection be made to basic structure. In some cases this connection is made to anodized parts. The resistance of an anodized connection is as unpredictable as a loose connection. The equalizing circuit has a low resistance and operates on low voltages. Since the total voltage is one-half volt across the ground circuit at full load and the equalizing circuit is to operate not on this entire value but on differences existing between generators, if a two per cent tolerance is to be maintained in load distribution, the equalizing circuit must be capable of operating on voltage differentials down to 0.01 volt, the drop resulting from a flow of two per cent rated current. The resistance of the winding in the regulator itself is approximately 0.4 ohm. For this reason it is important that the connection between the generator ground circuit and equalizer circuit of the regulator have a low resistance. Assuming that the lead resistance is no more than 0.1 ohm, the total resistance of the circuit for one generator is 0.5 ohm. With 0.01 volt across this value of resistance the current flow would be in the order of 20 milliamperes.

To minimize the effect of resistance changes of the generator ground circuit due to temperature it is desirable to use a material having a lower thermal coefficient of resistivity and a higher resistivity than copper. Such a material would result in a more constant resistance under all temperature conditions and a probable reduction in weight. Manganin, which is widely used in instrument shunts, is the most logical choice for this purpose. Manganin is at present a critical material. However, even during normal conditions its cost would probably prohibit its use. Nichrome was found to be a suitable substitute. Based on the use of 200-ampere generators, a resistor necessary to provide a one-half volt drop weighs only 10 ounces compared to approximately 3.5 pounds of wire used for the same resistance. Another advantage of having such a unit rather than a length of wire is that it can be calibrated before installation to a close tolerance. When a resistor of this type is used it is advisable to make the equalizer-circuit connection at the resistor rather than at the generator. This makes the connection across only a calibrated unit and eliminates the effect of the necessary connections in the circuit. The importance of this can not be stressed too highly since successful parallel operation depends primarily on the equality of the ground-circuit resistance.

Figure 5. Schematic wiring diagram, proposed system

1—Engine-driven generator
2—Reverse-current relay
3—Voltage regulator
9—Auxiliary power plant
L—Load center
⊖—Location for suitable circuit protector



From the standpoint of weight saving and wiring simplification, it is desirable to incorporate the ammeter shunt with the resistance unit. It is a simple matter to tap off a portion of the resistor which will give the same voltage drop as the ammeter shunt now being used. This value is usually 50 millivolts at 300 amperes. The addition of such a tap increases the weight of the resistor unit by a negligible amount, whereas the ammeter shunt weighs approximately 12 ounces. This eliminates an extra part as well as several connections.

Study of the foregoing shows that there are four major points to consider which affect load distribution between generators. They are:

1. The location of the reverse-current relay with respect to the system.
2. The length and size of connection between the reverse-current relay and the regulator.
3. The resistance value and equality of the generator ground circuits.
4. The total resistance and equality of the regulator equalizing circuits.

The power in such a system is drawn from the bus at various points; that is, at each nacelle and several points within the body of the airplane. There is no central power distribution point.

Proposed System

A desirable aircraft electric system would utilize all of the previously described equipment and eliminate the undesirable characteristics of the present system. As the result of tests conducted on the component parts and the system as a whole in the laboratory and on airplanes, a revised system,* shown in Figure 5, was proposed. The proposed system consists primarily of two networks, one a feeder network and the other a distribution network. All power is fed into and taken from a single point which is common to the two. The output of each generator is fed directly into the body of the airplane to a central point, where its reverse-current relay and voltage regulator are located, mounted adjacent on a panel. There is an individual panel for each generator. The output sides of the relays are connected by bus to the central power point. This constitutes the feeder network. A radial network distributes power to various load centers in the airplane.

Analysis of this system shows that regulator connectors of excessive and unequal lengths are eliminated. Line re-

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*** Navy Department Bureau of Aeronautics outlines a similar system for large aircraft in specification *SR-8*. Therefore, the proposed system is not entirely new except in its application to large Army Air Forces aircraft.**

The Gyrosyn Compass

O. E. ESVAL
ASSOCIATE AIEE

IN all military and commercial aircraft, the gyroscope is depended on for attitude and direction references. The vertical spinning gyroscope provides attitude indication in the form of the artificial horizon. At present, other attitude representations based on the vertical gyroscope are available. The use of the vertical gyroscope has been accepted for 15 years or more, and will be further improved for indication of roll and pitch but probably never superseded. The indication of azimuth, or heading of the airplane, which is actually the attitude around the vertical axis called direction, up to the present has had no standard means of presentation. In the past, magnetic compasses have been used for this purpose. These instruments for aircraft use are imperfect, and it has been found necessary to supplement this type of indication with a secondary reference in the form of a directional gyroscope. With the addition of the directional gyroscope, the inherent defects of the compass are greatly minimized. These defects are well known and can be stated as

1. Lack of stability.
2. Northerly turning error.

The directional gyroscope is essentially a gyroscope with the spin axis horizontal and mounted in gimbals with freedom about the vertical such that it tends to stay fixed in space around this vertical axis. A card fixed or geared to the vertical-axis gimbal will show changes in heading and for appreciable periods of time will provide a reference for maintaining a straight course. It is, however, subject to slow wander, and therefore must be corrected once in a while by manual resetting. This is done holding flight on a straight and level course. Then the conventional magnetic compass gives fairly reliable information and the directional gyroscope can be set to agree with its average reading. It is obvious that this system of periodically resetting the directional gyroscope to compensate for its random drift is a serious handicap to instrument flying. Also, in military aircraft, the usefulness of the magnetic compass in the cockpit is diminished because of the disturbances on the magnetic field due to the proximity of electrical components and armor plate.

Statement of Problem

The purpose of this paper is to describe, in a general manner, the development and characteristics of a system which largely eliminates these serious shortcomings. The first approach to a problem of this kind is to establish the basic requirements of a satisfactory direction-indicating system. The requirements are as follows:

1. The indication shall be stable in all conditions of air turbulence.
2. No manual resetting or attention on the part of the pilot shall be required.
3. The readings must be free from deviation and deviation changes.
4. Several repeaters of the indication must be provided for, each giving a stable or deadbeat indication.
5. The equipment must be simple, both mechanically and electrically, and all parts requiring periodic service must be easily accessible.
6. The accuracy must be commensurate with the limitations of the earth's magnetic field.
7. Turn errors must be negligible.
8. The entire system shall have minimum weight and volume.

In attacking this problem, several points stand out. It becomes apparent that it is necessary to operate from the earth's magnetic field at some part of the aircraft remote from magnetic disturbances and to transmit the indication to various points convenient to the flight personnel. The location for the magnetic sensitive unit should be in a part of the airplane where deviation and deviation changes are zero, or a minimum. Therefore, it must be away from guns, turrets, cargo, motors, and so forth. Such a location may, for example, be the very tip of a wing, the top of the vertical fin, or some part of the stabilizer. To enable mounting in such confined spaces, the unit itself should have the smallest possible physical dimensions. It also should be simple and rugged, to withstand the vibration and buffeting that is often severe in remote parts of the airplane. In addition, since such locations prob-

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sistance between relays is eliminated. All equalizing circuits have equal resistance. In such a system the disadvantages of the present system are overcome. In addition, a reduction in the weight of the electric system seems possible.

In any electrical service circuit protection against fault damage is usually employed. Circuit protection is particularly desirable in military aircraft because of possible damage from enemy gunfire. Parallel wiring has been advanced as a possible method of protection against failure of electric equipment due to damage of wiring from gunfire. Combat experience has indicated that such damage is not frequent. However, if the added precaution is desired parallel wiring can be applied to any system. Parallel wiring is a modification of any given system and should not be considered an additional system.

Locations for suitable circuit protectors are shown on Figure 5. The term "suitable protector" is used because no one type of protector is capable of performing the desired functions in every location. Various sizes and types will be required. Comparison of Figures 1 and 5 will show the difficulty in attempting comparable protection with the present system.

A comparison of the two systems shows that the desirable features of the proposed system are as follows:

1. There is a central distribution point from which all power is taken.
2. All power is fed into and regulated at this point.
3. Better load distribution between the generators is obtained because all generators are electrically symmetrical about the load center and do not have inherent circuit inequalities which the regulator must overcome. The only function the equalizing circuit of the voltage regulator is required to perform is that of compensating for irregularities in the voltage regulation.
4. Relays and regulators are accessible in flight for adjustment and replacement, if necessary.
5. Since each individual circuit on the airplane carries primarily its own load current, it is possible to provide adequate circuit protection for a given circuit without undue influence on other circuits.
6. Wire sizes of the power distribution lines can be reduced to take advantage of the central supply point to the extent that the effective resistance between the power supply and the load is decreased. This allows a smaller wire to be used in many cases where wire sizes are controlled by resistance. In many cases it is possible to rate the wire thermally allowing a reduction in wire weight. A comparison of the two systems as used on a typical four-engine aircraft shows that a weight reduction of approximately 38 pounds is effected by replacing connectors with smaller wire sizes wherever possible.

Observations on test airplanes employing the proposed system have shown that voltage regulation and power distribution are improved. Also, maintenance time required for the electric system has been decreased.

ably are somewhat inaccessible, the device must be particularly durable to eliminate frequent servicing of that part. All other parts subject to periodic servicing must be in the cockpit or other easily accessible location.

Gyrosyn Compass

The Gyrosyn compass provides one method of meeting the indicated requirements. This system consists essentially in automatically and continuously correcting the directional gyroscope for its random drift by indications derived from a direction-sensitive device located in a magnetically undisturbed region of the airplane. The need for manually resetting the directional gyroscope is eliminated, and it thereby serves also as a space-stabilized compass. The direction-sensitive device is termed a flux valve. It may be located in practically any part of the airplane because it is small. In this system, the directional gyroscope is synchronized by the flux valve to magnetic meridian. This explains why the name "Gyrosyn" was selected.

Vertical Gyroscope Analogy

It is interesting to note that this treatment of the directional gyroscope for direction indication is an exact analogy to the conventional use of the vertical gyroscope in which oscillations of short-period pendulums are integrated to establish accurate attitude indications without recourse to manual resetting. Why the directional gyroscope remained a "free-gyro" device for so many years after the vertical gyroscope was made to be controlled deserves explanation. First, simple devices such as the flux valve were unobtainable at the time the first gyro vertical was constrained to seek the vertical of its own accord, and also old-style magnetic compasses were fairly adequate until flying conditions became too exacting. Actually, controlled directional gyroscopes have been in use for a number of years, mainly in Europe. These, however, do not provide the simplicity of a short-period pendulum, are expensive to build, and while providing stable indications, fail to meet other requirements set forth above.

Description

A representative installation of the Gyrosyn compass is shown in Figure 1. It is noted that an alternate position of a flux valve is indicated. The system comprises two main units shown in Figure 2, Gyrosyn indicator (with amplifier), flux valve with connecting cable and optional repeater unit; Figure 3 shows the flux valve (close-up). The primary indication in this system is the directional gyroscope, and it is shown in the cockpit for use by the pilot as a flight indicator. This is the normal application. There are cases, however, in which the primary indicator may be allotted to some other member of the crew, such as the navigator. The sensitive pickup device embodying the flux-valve elements is shown in Figure 4. It is essentially a permeable-core device having an exciting winding and three phases spaced 120 degrees with signal pickup coils on each. A voltage is generated in the respective coils, depending on the relative position in the earth's field. By virtue of the fact that the output voltage changes sinusoidally with the position of each phase in the earth's field, it is possible to indicate the direction of the earth's magnetic field relative to the flux-valve element at a distant point. This is done by connecting the coils to respective phases of a self-synchronous unit located at primary indication location. Without going deeply into the theory of operation, it suffices to say that the induced voltage of the rotatable secondary winding of the self-synchronous unit has a magnitude and polarity determined by its angular displacement relative to the angular displacement between flux-valve element and the earth's magnetic field. By properly positioning the secondary winding, or rotor of the self-synchronous unit, angularly, a null signal may be obtained. This is the normal self-synchronous position. The rotor of the self-synchronous unit is linked mechanically to the vertical ring of the directional gyroscope. The electrical and mechanical schematic diagram of this system is shown in Figure 5. Shown also on this diagram is a precessing device which applies torque around the horizontal axis normal to the spin axis of the gyroscope. The precessional torque is controlled in magnitude and direction by the polarity and strength of the signal from the rotor of the self-synchronous unit, after amplification and rectification. The torque applied about the horizontal axis of the gyroscope simply produces rotation around its vertical axis, thereby driving the self-synchronous-unit rotor to a position where the signal becomes zero. In other words, the gyroscope is constrained to the position that maintains null self-synchronous-unit output. This means that the spin axis of the gyroscope will remain oriented in a fixed position relative to the earth's magnetic field, or magnetic meridian. The directional gyroscope, therefore, seeks the magnetic meridian in a manner very similar to the way in which a vertical gyroscope seeks the vertical. The only conditions under which the output of the self-synchronous unit is not null occurs when the gyroscope in its normal manner slowly drifts away from its correct azimuth position, or when the system is "turned on," preparatory to making a flight, that is, the period of

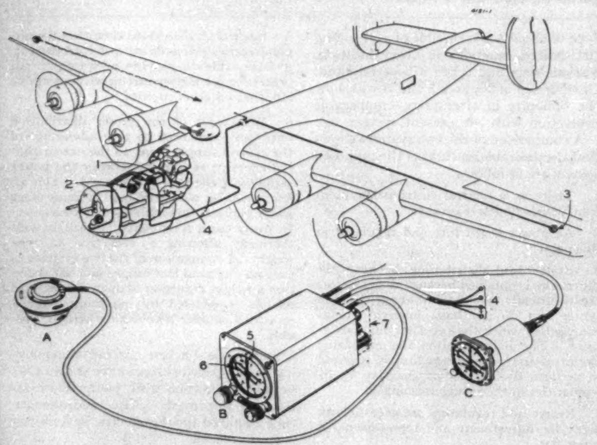

Figure 1. Airplane with Gyrosyn compass

Gyrosyn-compass system
C–1—Turn-indicator adaptation
Total weight with one flux valve and two repeaters $11\,^1/_8$ pounds estimated
A—Flux valve and mounting bracket—weight 1 pound actual
B—Gyrosyn-compass indicator—weight 8 pounds estimated
C—Gyrosyn-compass repeater—weight $1\,^1/_4$ pounds actual
1—Gyrosyn-compass indicator
2—Repeater
3—Flux valve
4—3 phase, 400 cycles; 115 volts, 27 volts direct current
5—Indicating pointer
6—Course setter
7—Cover

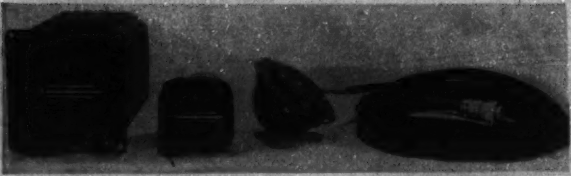

Figure 2. Gyrosyn-compass indicator (with amplifier), flux valve with connecting cable, and optional repeater unit

initial synchronizing. Heading changes are made without precessing the gyroscope and without losing synchronization. This is due to the fact that the magnetic meridian with respect to the flux valve and the directional gyroscope and self-synchronous unit with respect to the aircraft move through exactly equal angles, and therefore the null is not disturbed. The acceleration experienced in turn will temporarily disturb the null, but this will not produce a serious error, as described later. It is shown in Figure 5 that the flux valve in itself is a pendulum with two degrees of freedom. It is suspended on a universal joint in a manner normally to keep the plane of the flux valve horizontal. This must be done so that the indication obtained from this element represents the direction of only the horizontal component of the earth's magnetic field. Thus, a change in attitude of the airplane in roll or pitch cannot tilt the flux valve. It will remain in the horizontal plane. Certain disturbances, however, momentarily may swing it out of the horizontal. Such disturbances will make the signal fluctuate and give momentary erroneous data. In rough air, the signal may almost continually fluctuate. This brings us to the important function of the directional gyroscope as a primary indicator. It serves effectively to integrate all short-period disturbances and oscillations. The maximum precession rate as called for by the flux valve through the self-synchronous unit is limited. The period of pendulum, on the other hand, is so short that a single oscillation of the pendulum cannot be detected by observation of the gyroscope. The result in the Gyrosyn is that this system provides deadbeat accurate indications that are unchanged by any weather conditions.

Turning Error

So far, it has been shown that most of the requirements stated in the beginning can be met by a simple addition to the directional gyroscope, and by two extra elements, the flux valve and the amplifier. A deadbeat remote-indicating magnetic compass is obtained. Another of the requirements stated is the reduction of the northerly turning error. This error does exist in the Gyrosyn compass as in any magnetic-compass system that directly or indirectly is controlled by gravity. If the craft is turning, the resulting acceleration acts on the pendulum to displace it from a horizontal position. This results in an erroneous signal, the amount and direction of which depends on the instantaneous heading of the aircraft during the turn. The gyroscope, therefore, commences to move away from the magnetic meridian, but it does not do so rapidly. As a matter of fact, for a normal turn the duration is not sufficiently long to cause the gyroscope to be in error more than a few degrees, usually not more than five degrees. If the turn continues indefinitely, or, in other words, the airplane continues to circle, the error signal reverses on itself and does not cause accumulation of gyro errors. Rather, the gyro indication will slowly move back and forth about its correct position. It should be noted that definite limits are placed on the gyro precession rate. The precession characteristics against angle of error is nonlinear. This being the case, sustained oscillation of the pendulum resulting from air turbulence or poor manual flying, may cause desensitization of the coupling between flux valve and gyroscope. To overcome the oscillation, a small amount of damping is introduced on the flux-valve pendulum. It is accomplished by enclosing the element in a sealed chamber and filling with damping fluid. The damping coefficient may vary through wide limits without impairing the accuracy of the Gyrosyn compass, and therefore wide variations in temperature encountered do not, by changing the damping-fluid viscosity, affect the system operation.

Figure 3. Flux valve (close-up)

Alternate Turn-Error Corrections

In applications where the gyroscope is used as an automatic-pilot reference for rudder control, another means of limiting turn errors is usually available. Turns with an automatic pilot are initiated by a co-ordinated control of some sort. This control can be used to suspend the flux-valve signal for the duration of the turn. The gyroscope therefore becomes a free directional gyroscope with no constraint and will accumulate for the duration of the turn only a very negligible error because of its random wandering. At the resumption of straight and level flight, flux-valve control is automatically applied, and the gyroscope is again under the gentle influence of the flux valve. The same effect may be accomplished in a Gyrosyn system, independent of the automatic pilot by having the flux-valve signal interrupted by a rate-of-turn relay, such as a rate gyroscope. For most practical cases, however, the small temporary error induced with flux-valve control functioning during the turn is of no importance.

Accuracy

In stating the accuracy of the Gyrosyn compass as in any other type of remote-indicating magnetic compass, the conditions first must be fully defined. With variation of plus or minus 40 cycles in supply frequency and a corresponding change in voltage, both alternating and direct, and field strength change from 0.04 to 0.40 oersted, and the maximum error of definition is plus or minus $1^{1}/_{2}$ degrees from the magnetic meridian to the gyro indicator. Higher accuracy could be obtained but is hardly justified in view of the limitations of the earth's magnetic field. However, when electric generators and power supplies are improved to be more constant in voltage, frequency, wave form, and so forth, these performance figures will automatically be further improved.

Figure 4. Flux-valve element

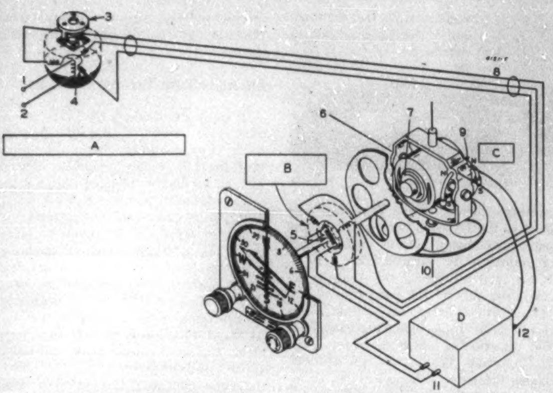

Figure 5. Schematic diagram of flux valve, self-synchronous units, gyroscope, amplifier, and precessor

A—Pendulously mounted flux valve
B—Signal transformer
C—Gyroscope
D—Amplifier
1—400 cycles
2—Excitation
3—Mounted to wing structure
4—Signal pickup coils
5—Rotor coupled to gyroscope
6—Horizontal axis
7—Gyro rotation
8—Connecting cable
9—Precessing coil
10—Vertical axis
11—Input: 800 cycles
12—Output: reversing-polarity direct current

Compensation

Primarily the size of the flux valve lends itself to mounting in very small recesses anywhere in the skin of the airplane. Therefore, in a number of types of airplanes, a location sufficiently remote from magnetic disturbances is available such that compensation is not required. This has been the experience with several standard types of airplanes, in which experimental Gyrosyn compass installations had been made. However, there may be some applications in which local deviations are obtained in every possible location of the flux valve. In these cases compensation must be resorted to. Conventional permanent-magnet compensators are available and add only a fraction of an inch to the over-all dimensions of the flux valve. Because the flux valve naturally will be located in a fairly inaccessible part of the craft, compensation by manual adjustment at that point may be impractical. It is possible to make available means for overcoming this difficulty. This is a method of applying compensation to the magnetic field by remote adjustment. A compensator consists of electromagnets distributed properly around the flux valve. The current in these coils is controlled by rheostats located conveniently in the cockpit or cabin. The compass may be swung either on the ground or in the air and compensated in a straightforward manner by adjustment of the potentiometers. This system is shown schematically in Figure 6. In cases where electric circuits are in close proximity to the valve, these circuits may be linked with the compensation circuit in such a way that full correction is obtained either "on" or "off."

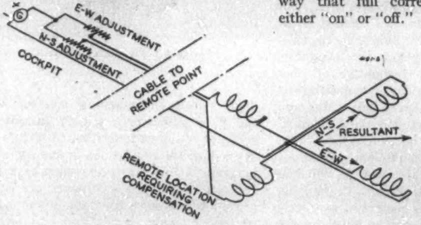

Figure 6. Compensation schematic diagram

Polar Navigation

The Gyrosyn compass obtains reliable operation in horizontal-field strengths greater than 0.04 oersted. However, in regions near magnetic poles, this field strength fades to the vanishing point. In the same region, directional indications obtained from the earth's field are unreliable and difficult to interpret, because of rapid changes in variation. These rapid changes make it necessary to obtain impossibly accurate position data to determine the correct local variations. This loss in magnetic directional information is not particularly serious to the Gyrosyn compass, since the flux-valve control may be turned off, leaving the directional gyroscope as a free gyroscope. Therefore, headings can be substantially maintained for hours or more of flight until reliable magnetic information again can be obtained through the earth's magnetic field. The flux-valve control of the gyroscope will be turned on and navigation resumed in a normal manner. In summing up the characteristics of the Gyrosyn compass in operation over widely varying field strengths, it is indicated that operation without adjustment is obtainable down to the region where magnetic indications become totally unreliable. Then, the precessing control of the gyroscope is suspended and a free navigational directional gyroscope remains.

Conclusions

The directional gyroscope has served many years usefully as an aircraft instrument. It has well-known shortcomings, but it fills a tremendously important place in the field of navigation. Its days of functioning as a free gyroscope subject to random wandering, however, are numbered. From now on, the directional gyroscope is going to be continuously controlled, and the direction-indicating instrument will require no more attention and manipulation from the pilot than the attitude-indication instrument, namely, the artificial horizon, has for a number of years. It is believed that the system described herein contributes a fair share in bringing about this change. No development of this sort can be said to be successful until it is received with approval by the armed services, airplane builders, and commercial operators, and most important, by the pilots themselves. The requirements for remote-indicating compasses that have been stated are an interpretation of the composite demand from all of these groups. It is believed, therefore, that in adhering to this set of requirements a satisfactory directional indicator is produced.

Electric Automatic Pilots for Aircraft

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AUTOMATIC pilots have been in use for many years in both commercial and military aircraft and are well suited to normal airplane control. Their greatest usefulness is in maintaining heading and attitude over long periods of time, with consequent reduction in strain on the human pilot.

Recently, however, with the development of bombardment tactics, the need for precise control has become more important. The functions of an automatic pilot for these applications are such that the maintenance of course, attitude, altitude, and trim are at times of paramount importance. It is necessary that the airplane, through the medium of the automatic pilot, respond quickly to turns as called for by the bombardier. Furthermore, it is desirable that the performance deteriorate as little as possible when the airplane is subjected to the disturbing action of turbulent air. This paper discusses the problems associated with such control and describes some of the features of a recent design.

Attitude Control

The minimum requirement of any automatic pilot is the angular stabilization of the airplane about three mutually perpendicular axes passing through the airplane's center of gravity and angularly fixed with respect to the surface of the earth. Motions about these axes are described as roll, pitch, and yaw. Rolling is rotation about the fore and aft, or longitudinal axis; pitching is rotation about the transverse, or lateral axis; yawing is rotation about the vertical, or directional axis. These motions are illustrated in Figure 1. Under certain conditions, the problem of simultaneous control about an airplane's three rotational axes can be reduced to three independent control systems. Each control system is then similar, and discussion will in general be confined to one axis.

Control of an airplane, manual or automatic, is accomplished through the deflection, or angular rotation of the airplane's control surfaces about the surface hinge line. Rotation of a control surface is, in effect, a change in angle of attack of the surface and gives rise to an aerodynamic force normal to the surface. The control surface forces, in turn, create control moments about axes through the center of gravity of the airplane. The angular acceleration caused by a control moment continues until the resulting damping and stability moments are equal to the control moment. The angular rotation tends to cease when the control moment is equal to the static stability moment. A given control moment therefore is opposed by an inertia moment, a damping moment, and a static stability or spring moment. The magnitude of these moments varies considerably between axes and for different flight conditions. If control moment is maintained until appreciable changes in flight path occur other moments are generated which, however, may be disregarded here.

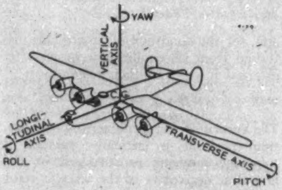

Figure 1. Schematic drawing of airplane showing its three translational axes and the three angular degrees of freedom

For short-period motion, the application of pitching-control moment and the resulting rotation about the transverse axis may be expressed as

$$M_c = I\ddot{\theta} + C\dot{\theta} + K\theta \qquad (1)$$

where

M_c = applied control moment about the transverse axis
I = moment of inertia of the airplane about the transverse axis
C = damping coefficient, or damping moment per unit rate of pitch
K = stability coefficient, or restoring moment per unit angle of pitch
θ = angular displacement in pitch

The foregoing equation is subject to several restrictions. It is approximately correct for small angles of pitch and describes the angular motion only for a short time after control moment is applied. The static longitudinal stability, actually a function of change in angle of attack, is assumed to depend on the angle of pitch. It is further assumed that the coefficients C and K are constant for a particular condition of flight.

For automatic stabilization, the control moment becomes a restoring moment, M_r, and is made a function of the angular error, θ, between the airplane attitude and a gyroscopic reference axis. The restoring moment applied by the automatic pilot then acts to reduce the error to zero in a manner determined by the combined characteristics of airplane and automatic pilot. Assume that the airplane is coerced from its equilibrium position and then released. It is then acted upon by the restoring and the static stability moments such that

$$M_r = f(\theta)$$

and

$$-f(\theta) - K\theta = I\ddot{\theta} + C\dot{\theta} \qquad (2)$$

If the restoring moment is proportional to the error alone, as in many automatic controls,

$$M_r = f(\theta) = K'\theta$$

and

$$-(K+K')\theta = I\ddot{\theta} + C\dot{\theta} \qquad (3)$$

The static restoring moment, $K'\theta$, supplied by the automatic pilot, acts to in-

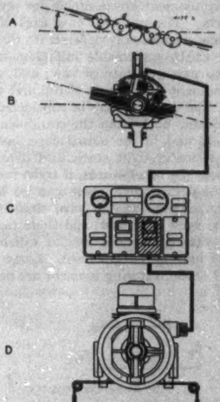

Figure 2. Pictorial representation of the *A*-5 automatic pilot controlling the airplane in roll

A—Roll angle
Airplane deviates in roll
B—Vertical gyroscope—horizontal gimbal ring and roll pick-off measure roll angle
C—Amplifier rack—roll signal is amplified and derivatives are taken
D—Servo unit deflects ailerons
E—Airplane returns to level flight

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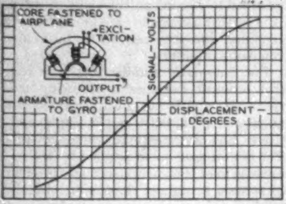


Figure 3. Schematic diagram of signal pick-off and its voltage–angle characteristic

crease the static longitudinal stability, $K\theta$. Where "tight" control is necessary, the stability furnished by the automatic pilot must be several times larger than the inherent stability of the airplane. Control which is proportional to the displacement error alone is known as error control.

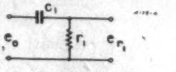
When K' is sufficiently large, and

$$C<2\sqrt{(K+K')I}$$

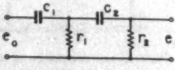
the angular motion is a damped oscillation and undamped natural frequency is

$$f_n=\frac{1}{2\pi}\sqrt{\frac{(K+K')}{I}}$$

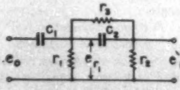
Reasonably large values of static restoring moment are desirable for several reasons. The roll and yaw axes of an airplane are mutually dependent; that is, there exists appreciable rolling moment due to yaw and rate of yaw, and yawing moment due to rate of roll and sideslip. The two axes may be considered independent, both from the standpoint of analysis and in the actual case, only if each is under tight static and dynamic control. In other words, if tight rudder control is provided, there can be little rolling moment due to yaw, since there can be very little yaw, and there can be little yawing moment due to sideslip if rolling motion is suppressed. Large values of static restoring moment are necessary too, when any sustained disturbance is encountered, such as an engine failure in multiengine ships; a sudden sustained change in trim, as when a large bomb is released; or a long-period air disturbance.



A. First-derivative network



B. Second-derivative network



C. First- and second-derivative network

Figure 4. Derivative-taking networks

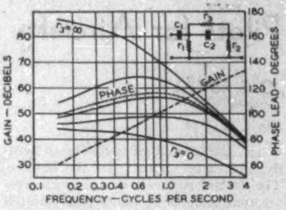


Figure 5. Phase curves of the network of Figure 4C for different values of r_3

The broken curves are measured phase and gain for the rate channel of the amplifier

Additional damping moment is obtained by making the restoring moment proportional to the time derivative of the angular error as well as the error itself, so that

$$M_r=f(\theta,\dot{\theta})=K'\theta+C'\dot{\theta}$$

and, for the condition of an airplane disturbed from its equilibrium position by external forces and then released,

$$-(K+K')\theta=I\ddot{\theta}+(C+C')\dot{\theta}$$

The return of the airplane to its equilibrium position is critically damped if sufficient moment proportional to the first time derivative of the angular error is available, or

$$(C+C')=2\sqrt{(K+K')I}$$

This type of damping is often called error-rate damping.

The return of the airplane to level attitude after it has been subjected to a disturbance must be at least critically damped because of the nature of the disturbances encountered in rough air. Disturbing moments, having frequency components varying over the range from nearly zero to that well beyond the natural frequency of the control, may be encountered. If the system is not critically damped, air gusts having frequency components near the natural frequency of airplane and its automatic control may force abnormal deviations, and what appears to be continuous oscillation results.

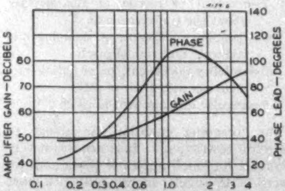


Figure 6. Over-all amplifier phase and gain curves

The requirement of large static restoring moments imposes the necessity for rate control. The damping provided by rate control limits the angular deviations due to air gusts, and makes possible close automatic control in rough air. If the airplane is subjected to a sinusoidal gust or disturbing moment,

$$M_d=M\sin\omega t$$

the resulting maximum deviation is given by

$$\theta_m=\frac{M}{\sqrt{(C+C')^2\omega^2+[I\omega^2-(K+K')]^2}}$$

A properly proportioned static and rate control together with the airplane's inertia will go far to suppress the deviations throughout the entire range of disturbing frequencies.

For very slow disturbances where ω approaches zero,

$$\theta_{m(\omega\to0)}=\frac{M_d}{K+K'}$$

and, for the condition of critical damping,

$$\frac{\theta_m}{\theta_{m(\omega\to0)}}=\frac{1}{1+\left(\frac{f}{f_n}\right)^2}$$

where f_n is the undamped natural frequency. Since the ratio can never exceed unity, the deviations are greatest for disturbances that approach zero frequency. The deviations are mainly limited by the static restoring moment at very low frequencies, by the damping at frequencies near the natural frequency, and by inertia at high frequencies.

The foregoing discussion assumes that control moments proportional to the angular deviation and rate of deviation are applied to the airplane with no time lag between their occurrence and the correcting moments. The measurement of error electrically from a free gyroscope is accurate and instantaneous. The derivation of rate of error from the original displacement error can be obtained electronically with any desired response, depending upon the permissible amplifier gain. However, some time delay is usually present in the process of power amplification and movement of the control surface, principally because of power limitations on the servo unit. Time delay in the application of control moment is important only in relation to the time of response required of the airplane. Where tight control is essential, time delay is usually the limiting factor. Error control, if applied through a system involving appreciable time delay, may cause continuous oscillation, depending upon the damping provided, since the

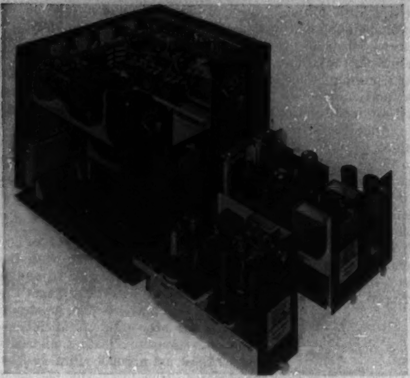


Figure 7. The amplifier rack with removable flight amplifiers

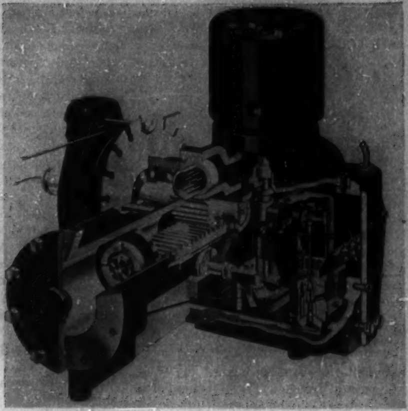


Figure 9. Cutaway view of the servo unit

restoring moment then has a component in quadrature (instead of exactly 180 degrees out of phase) with the airplane's angular position, and there is some net energy per cycle supplied to the system. When rate control is applied through time delay, the control moment actually applied to the airplane has a component out of phase with the airplane's angular position. The rate control degenerates partially to error control and the natural frequency may increase considerably, making the need for rate even greater.

If most of the time lag from gyroscope to control-surface hinge moment is concentrated in a single energy-storing system, represented electrically by a series resistance–capacitance network, or mechanically by a viscous friction-spring system, it can be shown that the effect of such lag can be compensated almost completely by the addition of an acceleration or second-derivative signal. The voltage at the input to the servo unit therefore is made proportional to the sum of displacement error and the first and second derivatives of the error.

Error Measurement

Measurement of pitch and roll error is accomplished through the use of inductive "pick-offs" as shown schematically in Figure 3. The primary winding, or center leg, is excited with a 400-cycle voltage. The secondary windings, or outer legs, are connected in series opposing. Angular motion of the airplane with respect to the gyroscope varies the coupling between primary and opposing secondary windings, so that the voltage induced in one secondary winding increases while the voltage in the other decreases. The output voltage is linear with angular rotation and has the phase of the secondary winding with the larger voltage. A curve of pick-off voltage against angular rotation is shown in Figure 3.

This phase-reversing signal pick-off may be described alternatively as a carrier-suppressed or balanced modulator, where the 400-cycle voltage is the carrier and the airplane angular motion is the signal. The carrier is resupplied at the amplifier in the form of a 400-cycle plate voltage to effect "phase detection" or demodulation.

Derivation of Rate and Acceleration Signals

The input signal to the amplifier is a measurement of airplane angular displacement only. This signal is separated into two channels. One channel provides a displacement component, and the other is phase-detected and applied to the differentiating network of Figure 4C. The voltage e_{r1}, appearing across resistance r_1, is a measure of the time derivative of the demodulated signal voltage e_0. When the single-mesh resistance–capacitance circuit of Figure 4A is considered in more detail,

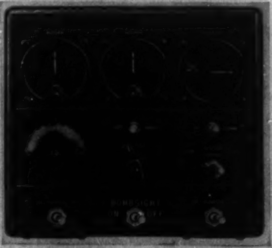


Figure 8. A-5 automatic-pilot control panel

$$e_o(t) = e_{c1} + e_{r1}$$

If $e_{c1} >> e_{r1}$, then

$$e_o(t) = e_{c1}$$

and

$$i = c_1 \frac{de_o}{dt}$$

therefore

$$e_{r1} = r_1 i = r_1 c_1 \frac{de_o}{dt}$$

If the airplane were hunting at its natural frequency when under automatic control,

$$e_o(t) = E_o \sin \omega_n t$$

and

$$\frac{de_o}{dt} = \omega_n E_o \cos \omega_n t$$

Also,

$$e_{r1} = \frac{r_1 e_o}{\sqrt{r_1^2 + \frac{1}{\omega_n^2 c_1^2}}} \sin(\omega_n t + \phi)$$

where

$$\tan \phi = \frac{1}{\omega_n r_1 c_1}$$

When r_1 is small compared to $1/\omega_n c_1$, ϕ approaches 90 degrees and

$$e_{r1} = r_1 c_1 \omega_n E_o \cos \omega_n t = r_1 c_1 \frac{de_o}{dt}$$

Therefore, when the capacitative reactance, x_{c1}, at the natural frequency of the control system is large compared to r_1, the phase of e_{r1} is advanced nearly 90

Table I. A-5 Automatic-Pilot Sensitivities in the B-24E

Axis	Control Surface	Pounds Cable Force Per Degree Of Error
Roll	Aileron	.40
Pitch	Elevator	.48
Yaw	Rudder	.63

degrees ahead of e_o. A similar phase lead is possible with a series resistance and shunt inductance, but at these frequencies the large reactance required for effective vacuum-tube operation makes the physical size of the inductance prohibitive.

If we refer to Figure 4B, by the same reasoning e_{r_1}, the voltage across r_2 is a measure of the second derivative of e_o, provided the impedance $\sqrt{r_2^2 + x_2^2}$ at the natural frequency is large compared to r_1. Any desired combination of de_o/dt and de_o^2/dt^2 is obtained by shunting C_2 with the resistance r_3 as in Figure 4C. Phase curves for the network of Figure 4C and measured phase and gain curves for the rate channel of the amplifier are shown as a function of frequency in Figure 5. With r_3 short-circuited, the phase lead approaches 90 degrees at zero frequency, and with r_3 open-circuited the phase lead approached 180 degrees at zero frequency. Phase and gain curves for the entire amplifier are shown in Figure 6.

Servo Unit

The amplified displacement and first- and second-derivative voltages are impressed across the input terminals of the servo unit, or power unit. The servo unit is a self-contained electrohydraulic system which applies cable force proportional to amplifier-output voltage. A hydraulic-pressure repeat-back is incorporated in the servo unit whereby the hydraulic pressure developed in the cylinders, acting across a small valve-stem area, is matched against the force developed by an electromagnetic torque motor. For a given voltage applied to the torque motor, the servo unit tends to maintain constant pressure in the cylinders, regardless of drum position. The servo drum operates at maximum speed until the force balance is attained. Since for a given amplifier output the servo unit tends to maintain a proportional force output, when no signal voltage is applied, it tends to maintain zero force output, and the servo drum moves freely when subjected to external forces. While the servo unit is capable of providing the large forces and speeds required, the average power drawn from the airplane's power supply is kept at a minimum, because pressure is developed only when called for by the signal.

Force Repeat-Back

The force repeat-back principle of aircraft control, as provided by the servo unit, merits some discussion as to the reasons for its use. The aerodynamic force acting on a control surface depends upon the angular deflection of the surface from its neutral or weather-vane position. Neglecting the small positive and negative damping forces acting on the surface because of its rotation and rotation of the airplane, this force is proportional to the surface deflection for small angles. If the airplane's cable-system friction is neglected, therefore, the surface force, hinge moment, and cable force are proportional to their respective deflections.

The control-system frictional forces normally are kept small in order that the human pilot may retain the "feel" of the airplane. Satisfactory manual control is obtained when the ratio of aerodynamic force to friction force is high. The principal load on the control cable therefore may be considered that of a spring. The zero point of the spring, however, shifts over a wide range, depending upon power, loading, and any unsymmetrical conditions which may arise. The spring stiffness is not constant but increases as the square of the air speed. A given force applied to the control cable will result in different deflections of the surface depending upon the air speed, but will result in a fixed control moment independent of air speed. When the servo member of the automatic pilot is made to exert a force on the control cable proportional to error signals, tight control may be retained throughout a large speed range.

Automatic Trim

When the center of gravity of an airplane shifts longitudinally, or when the propeller thrust or the air speed is changed, moments are created about the transverse axis which require a steady elevator-control-cable force for their compensation. Constant biasing moments such as these are normally furnished by the elevator "trim tab." By means of the tab the elevator is shifted to a new angular position, reducing the elevator-cable force to zero. Since the control-surface position at which zero resultant control moment occurs is variable, it is convenient that the servo force be independent of servo position.

Automatic operation of the trim tab in a manner to maintain the average servo-force zero is provided only in the elevator system, where frequent changes in trim occur. The trim-tab control responds to the average elevator servo force, and hence to the average pitch error, θ. The restoring moment provided by the trim-tab control is approximately proportional to the time integral of the pitch error. The total restoring moment about the transverse axis is then

$$M_r = \int \theta dt + K'\theta + C'\dot{\theta}$$

Control of Altitude and Course

When constant altitude is required, the pitch attitude, and hence approximately the rate of climb or descent of the airplane, is varied according to the alti-

Figure 10. The constant-altitude control

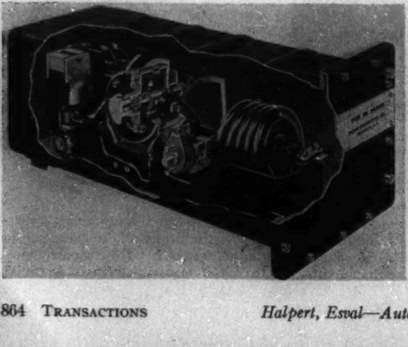
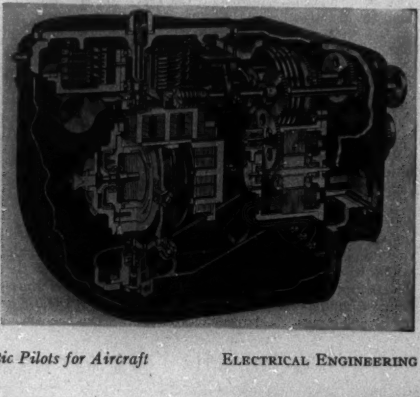


Figure 11. Cutaway view of the azimuth gyroscope



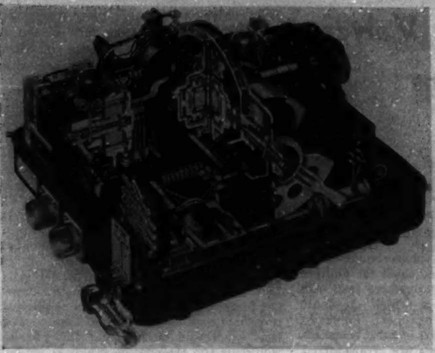


Figure 12. Cutaway view of the vertical gyroscope

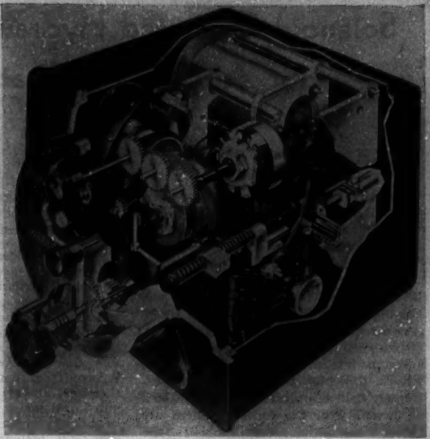


Figure 13 (right). The turn control

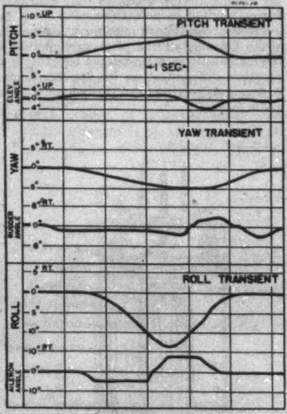


Figure 14. Recordings of transient flight conditions in the *B-24E*

Altitude 6,500 feet; indicated air speed 175 miles per hour

tude error. Since the natural frequency of the airplane position about an altitude reference is low compared to the natural frequency of the pitch control, very close control of altitude is possible by using only altitude error signals. Normal sensitivity of the altitude control is one degree of pitch change per 20 feet of altitude error. The altitude reference is a barometric element, and changes in static pressure are caused to modulate an alternating voltage in a way similar to that already described. This altitude-error signal voltage biases the pitch signal to provide the required changes in pitch attitude. Figure 10 shows a cutaway drawing of the altitude control.

The airplane is maintained on course by setting in heading changes according to departures from some reference on the ground. Heading changes may be very small or may consist of turns of more than 360 degrees. For this reason, heading errors are measured by a synchronous signal transformer, the stator of which is driven from the vertical ring of the azimuth gyroscope. The stator windings of this signal transformer are connected to the stator of an identical unit in the turn control, the rotor winding of which provides the heading-error signal. Either rotor may be rotated to shift the azimuth index, thereby effecting a change in heading. The airplane is automatically banked by shifting the roll index an amount corresponding to the rate of change of heading or rate of turn. The airplane responds quickly to a change in azimuth index, for the control moment provided by the rudder is proportional both to the amount of index change and to the rate of index change.

Performance

The performance of the automatic pilot is best indicated by giving static sensitivities in cable pull per degree of airplane deviation, and reproducing records of transient flight conditions. A table of sensitivities for a typical installation in a *B-24E* "Liberator" is given in Table I.

The flight records are measurements of attitude error and control-surface position in their correct time relationship. The "flight recorder," in measuring attitude, has a dynamic error of less than one tenth of one degree under these conditions of operation. Surface position is recorded by direct mechanical connection to the cable system.

A transient is put into the system by manually overpowering the automatic pilot, holding this condition until considerable attitude error is established, and suddenly removing the manual effort. Typical transients are shown in Figure 14. In each case there is a relatively long period of approximately constant control-surface deflection while the error is increasing. At the end of this period, the system is freed and the error becomes zero in a relatively short time. In each case the airplane returns to its original attitude in an approximately critically damped fashion. The records show that the damping supplied by the automatic pilot is greatest in yaw, a fact which should be expected since the airplane has its greatest inertia and least damping around its vertical axis.

Conclusions

An airplane may be stabilized accurately about its three rotational axes, provided proper error and error-derivative measurements are utilized. High static restoring moments are applied by the automatic pilot with sufficient derivative control to provide approximately critical damping. The static resorting and damping moments supplied by the automatic pilot, together with the airplane's inertia, effectively suppress the deviations over the full range of disturbing frequencies due to rough air. The force repeat-back system permits tight control over a wide range of air speeds and provides a convenient method of automatically maintaining elevator trim.

Solenoid-Operated Hydraulic Valves for Aircraft Applications

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NONMEMBER AIEE

DURING the past several years there has been a definite trend by some aircraft manufacturers to eliminate hydraulic controls from combat airplanes. The vulnerability of hydraulic systems used on airplanes manufactured during this period and to a certain extent today are big factors in promoting this trend. Space requirements in the cockpit of an airplane to mount control valves and accommodate necessary piping to these valves, difficulty with packings at extremely cold temperatures, and the lack of flexibility of the control system are other factors that contributed to the aforementioned trend.

The changeover, however, has been slow because hydraulically operated controls have a number of advantages over other types in applications where considerable work must be done during a short period of time.

As soon as the shortcomings of the standard hydraulic systems relative to combat airplanes became apparent, considerable effort was applied to overcome them. Improved packings for low-temperature operation, hydraulic fuses that isolate any portion of a hydraulic system that has a ruptured hydraulic line, and lightweight solenoid-operated control valves, the advantages and description of operation of which will be given in detail later, are all recent developments that should bring about further consideration of hydraulic controls in applications in which considerable power is required.

Some of the major advantages of solenoid-operated valves are as follows:

1. Manually operated valves require either a rotary or reciprocating shaft for plunger or poppet actuation; therefore, seals are required at the point at which the shaft protrudes from the valve proper. Forces to move poppets or plungers in most solenoid-operated valve designs are transmitted into the valve without any mechanical linkage from outside the valve proper; therefore, all except static seals are eliminated. Although considerable work has been done on hydraulic seals in recent years they still present a troublesome service problem because of deterioration and wear.

2. Manually controlled valves must be accessible; therefore, they are mounted at the control point, such as the cockpit of an airplane. A visual inspection of the control panel of an airplane will show a maze of dials, buttons, and levers on the front side of the panel, and an even greater maze of wires and hydraulic piping at the rear. A solenoid-operated valve may be actuated by a simple switch on the control panel and may be placed in a position that permits short hydraulic line lengths. The total weight and vulnerability of the system consequently are greatly reduced.

3. Since solenoid-operated valves are electrically controlled, they lend themselves to semiautomatic and automatic operation. This feature is of particular importance in combat airplanes where automatic operations will relieve the pilot of at least some mental and physical effort. Automatic operations in which solenoid-operated valves are used are now under test by the Army Air Forces.

Although solenoid-operated valves have been used rather extensively in manufacturing processes and machinery, they are comparatively new in aircraft applications. The application of solenoid-operated valves to aircraft presented many new problems that were not present in previous applications. Some of these problems and means used to overcome them are as follows:

1. Plungers on plunger-type valves may be pressure balanced, so that they can be moved relatively easy, but these valves are difficult to make with leakage rates low enough to be acceptable to the aircraft industry. The leakage rate of a plunger-type valve also increases with usage as the plunger and valve body wear, even if it is hand fitted or lapped to give a low rate of leakage initially. Poppet valves eliminate the leakage objection; however, if the poppets are large enough to permit satisfactory flow at permitted pressure drops, the forces required to unseat the poppets at pressures now used in aircraft are so high that a solenoid designed for this purpose would be too large to be practical for aircraft usage.

2. To bring the forces required to operate poppet valves within reasonable limits, pilot valves are used. Pilot valves are also poppet valves, but port areas of these valves are comparatively small, so that hydraulic forces tending to hold them closed can be overcome readily by a solenoid of reasonable size. Opening of the pilot valve initiates movement of the main poppet by causing a pressure differential on its opposite ends. A detailed description of this action will be given later.

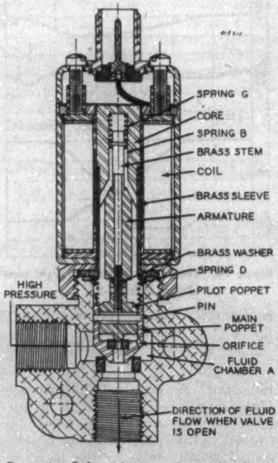

Figure 1. Solenoid-operated normally closed two-way hydraulic valve

A valve of this type weighing approximately one pound will function continuously at pressures as high as 2,000 pounds per square inch. The solenoid will draw one ampere at 24 volts. At a flow of five gallons of *AN-VV-O-366b* fluid per minute the pressure drop will be 20 pounds per square inch

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When a satisfactory method of angular control is established, the control of altitude and course according to any desired reference is readily attained.

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3. The development of hydraulic oils with comparatively flat viscosity curves also has promoted the use of pilot-operated valves. Pilot operation of valves depends on pressure differentials brought about by opening at least one orifice to permit escape of fluid in sufficient quantities to cause a pressure drop. The chamber from which escape of fluid is permitted is also supplied fluid through a small fixed orifice and clearance around the poppet. As temperatures decrease the oils become more viscous, thereby affecting the flow of fluids through the orifices and clearances, which in turn affects the differential in pressure used to move the main poppet. However, if the viscosity can be kept within reasonable limits the temperature range in which the valve will function satisfactorily is adequate.

A solenoid-operated hydraulic valve design that has proved quite satisfactory is shown in Figure 1. This design is a pilot-operated poppet type. With reference to Figure 1, this valve functions as follows:

We will assume that the valve is closed with fluid under pressure in fluid chamber *A*. Fluid in the space above the main poppet, pilot poppet, and armature also will be under pressure, since this space is open to chamber *A* through the orifice in the main poppet and clearance around the pilot poppet and armature. In the valve shown, the main poppet requires a tractive effort of approximately 120 pounds to unseat it at 1,500 pounds per square inch pressure, whereas, the pilot poppet requires a tractive effort of only three pounds to unseat it at this same pressure.

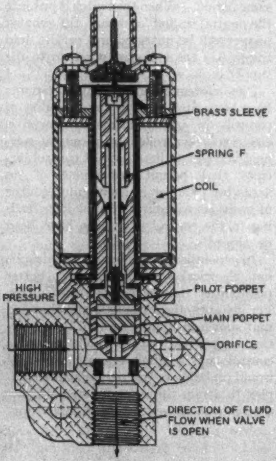

Figure 2. Solenoid-operated normally open two-way hydraulic valve

The size, weight, and functional characteristics of this valve are the same as those described for the valve shown in Figure 1, except that the maximum opening pressure is 1,700 pounds per square inch

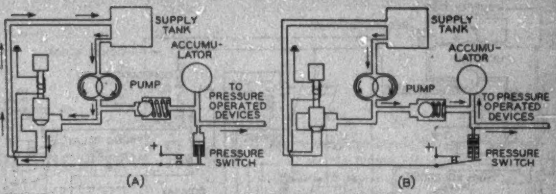

Figure 3. Control circuit for a normally open solenoid-operated valve used as a pressure regulator

A. Solenoid-operated valve in normal position. Pump by-passing fluid at no pressure

B. Solenoid-operated valve in energized position. Pump-filling accumulator

When the coil is energized the armature will move along the brass stem away from the brass washer at the bottom toward the core. During movement of the armature, spring *B* is compressed. At a point where the air gap between the armature and the core is slight, the armature strikes an annular shoulder on the brass stem attached to the pilot poppet and unseats it. Unseating of the pilot poppet permits escape of fluid from the space above the main poppet. Although a small amount of fluid flows into the space above the main poppet through the orifice shown, the pressure drop in this space will be sufficient, so that the high pressure on the area below the main poppet will move it upward. The armature then will be in direct contact with the solenoid core. Spring *B* which was compressed during initial movement of the armature, then will move the brass stem attached to the pilot poppet upward. The pilot poppet which is attached to the main poppet by means of a pin that permits approximately 0.040 movement between them picks up the main poppet and opens it wide. Spring *C* is comparatively light, and spring *B* must be stronger than spring *D* so as to permit full opening of the main poppet. The total time required for opening this valve is less than one-tenth second at normal temperatures.

When the coil is de-energized spring *D* will seat the large poppet and spring *C* will seat the pilot poppet.

Figure 2 shows a valve of similar design except that it is open in the unenergized position. When the coil is energized the armature moves downward, compressing spring *F*. As the armature moves downward it seats both the pilot poppet and main poppet. When both poppets are seated there is a gap between the armature and the head of the brass stem on which the armature slides. When the coil circuit is opened spring *F* moves the armature upward. The force of spring *F* and the impact force of the armature then will open the pilot valve. The maximum opening pressure of this valve is dependent on spring *F*. Sequence of operations to open the valve fully from this point are the same as those described for the normally closed unit.

The impact feature built into these valves adds approximately 30 per cent to the maximum pressures at which these valves open.

Various combinations of two-, three- and four-way valves with ports normally open, normally closed, or a combination of normally open and normally closed can be manufactured to fit any particular application.

Some typical circuits that may be used to control the operation of these valves are shown in Figures 3 and 4.

Figure 3 shows a normally open solenoid-operated valve, same as that shown in Figure 2, used in conjunction with a pressure switch to regulate the flow of fluid to a hydraulic accumulator. A snap-action switch with a slight range of travel between the "off" and "on" position, for instance, might keep the accumulator charged to a pressure of from 800 to 1,000 pounds per square inch. Pressure switches of this type are now available.

Figure 4 is a schematic drawing showing a three-way valve with a normally closed pressure poppet and a normally open exhaust poppet used to charge automatically a gun of the 0.50-caliber *M*-2 Browning type. This system will automatically charge a gun, when it fails to fire, if the trigger switch is depressed. It also provides means to "safety" the gun, that is, hold the bolt in the retracted position so that it cannot be fired.

The hydraulic system, with the exception of the three-way valve, is common to all airplanes that have hydraulic systems. The trigger switch, firing solenoid, and firing relay are common to all airplanes having an electrical firing control.

Three relays and an electrolytic condenser are required in the control box. One control box is required per gun. A normally open switch, that is, actuated by the gun bolt, is mounted on a hydraulic actuating cylinder mounted on the gun.

Any number of guns may be controlled from one common switch in the cockpit; however, a control box, hydraulic actuating cylinder, gun switch,

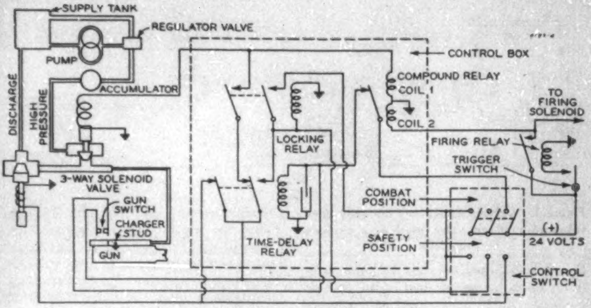

Figure 4. Control circuit for a fully automatic gun-charging system plus means to "safety" guns

and three-way valve are required for each gun.

When the control switch is placed in the "combat" position the time-delay relay is energized by a closed circuit through the control switch and the normally closed contacts of the compound relay. The normally open set of contacts that controls the locking relay coil therefore is closed, the locking relay coil energized, and its normally open sets of contacts closed. The only purpose of the locking relay is to prevent a partial charging cycle when the combat switch is closed initially. From this point on the locking relay will be energized until the setting of the control switch is changed. The two sets of normally closed contacts of the time-delay relay that control the coil circuits of the valve are then open. The system will stay in this condition until the firing switch is closed.

When the trigger switch is closed, coil number 2 of the compound relay is energized, thereby opening its contacts and breaking the circuit to the time-delay relay coil. The normally closed contacts of the time-delay relay will not close immediately because of the condenser across its coil. If the gun then fires, the charger stud will actuate the gun switch once per round fired, thereby maintaining the time-delay relay in the energized state with current impulses. If the gun fails to fire after a predetermined time interval, the two pairs of normally closed contacts of the time-delay relay will be closed and both coils of the valve energized. The exhaust port of the valve then will be closed and the pressure port open. Fluid under pressure is then free to flow into the actuating cylinder which retracts the bolt to its fully recoiled position. In the latter position the gun switch will be made, thereby closing the circuit to the time-delay relay which opens the circuits to the solenoid coils of the valve, which again assumes its normal position and permits the fluid in the actuating cylinder to return to the supply tank. A quick release mechanism on the charging cylinder holds the gun bolt in the recoiled position and keeps the gun-switch circuit closed until fluid in the actuating cylinder is very nearly exhausted. If the gun does not fire again in the predetermined time after completion of a charging cycle, the operation will be repeated if the firing circuit is still closed.

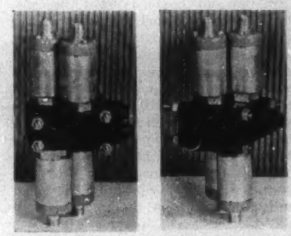

Figure 5. Solenoid-operated four-way hydraulic valve commonly used to control a double-acting cylinder

Weight—$4^1/_4$ pounds. Will handle flows up to six gallons per minute at a maximum pressure drop of 35 pounds per square inch

Relay-coil number 1 of the compound relay assures completion of any charging cycle that is initiated, irrespective of the condition of the firing trigger. This coil is hooked into the coil circuit that controls the high-pressure poppet and can be opened only when the gun switch is made at the end of the actuating-cylinder travel.

When the control switch is placed in the "safe" position, the locking relay is immediately energized, and, since the contacts at the time-delay relay are closed, the coil that controls the high-pressure poppet will be energized to unseat it and permit fluid under pressure to flow into the actuating cylinder. The circuit to the coil that controls the exhaust poppet is closed directly as the control switch is put into the "safe" position and, therefore, will remain seated as long as the switch is in this condition. As the actuating cylinder retracts the gun bolt, it will make the gun switch at very near the end of its travel. Closing of the gun switch will energize the time-delay relay coil, the contacts of which will open and break the circuit to the coil that controls the high-pressure poppet which will then seat.

Since the coil that controls the exhaust poppet remains closed as long as the control switch is in the safe position, the gun bolt will be held retracted during this same period. When the switch is put into the neutral or "off" position, the exhaust poppet will be unseated and permit fluid trapped in the cylinder to return to the supply tank.

The aforementioned control circuits have been cited to show the flexibility of controls in which hydraulic power is electrically controlled by means of solenoid valves. Other types of control circuits that permit positioning of an actuating cylinder in an infinite number of positions without hydraulic lines leading to the control point also have been developed.

In conclusion, a combination hydraulic and electrical control may offer better possibilities than either one used alone. For instance, the automatic gun charging unit shown in Figure 4 would be a practical impossibility with a wholly hydraulic control, and a wholly electrical control would probably be heavier and more complex to attain the same functional characteristics obtained by the combination.

The Scientific Basis for the New British System of Cockpit Lighting

EDWARD S. CALVERT
NONMEMBER AIEE

Synopsis: The new British system uses ultraviolet for the illumination of the instrument markings and red floodlighting for the general illumination of the cockpit. The scientific basis for this system, which is known as the dual system of cockpit lighting, is discussed, and some details of the fittings are given. Particular emphasis is laid on the layout of the cockpit, correct layout being regarded as the most important single factor for the success of the whole system.

In order to operate an aircraft at night, and particularly a military aircraft, with the maximum comfort, safety, and efficiency, the pilot must be able to read his instruments at a glance, but the method used to render the instrument markings legible must not be such as to reduce appreciably his ability to see objects outside the aircraft. Anyone who has tried to look out of a lighted room at night knows that the brightness level inside the room must be low if anything much is to be seen outside. This was realized from the beginning of cockpit lighting, and the markings on the instruments until recently have been self-luminous and could be read, although with some difficulty, without any lighting at all. However, as controls, switches, radio equipment, messages, maps, and so forth also must be observed or read at times, it has in the past been the custom to install a system of general lighting, usually white, for use when required, these lights being controlled by means of dimmers.

Had the right color been chosen for the self-luminous paint and for the lighting, and had cockpits been laid out so as to make it possible to put the lights in reasonable positions for good illumination, then, operationally this system would have been satisfactory. It is, however, the fact that the wrong color, that is, green, was chosen for the self-luminous paint, and that the right color, that is, red, was not always chosen for the lighting. Also, until night fighting forced attention to the problem the lighting was not considered to be important enough to be taken into account in the initial layout of the cockpit, with the result that some types of aircraft were built with cockpits which, without extensive structural alterations, could not be illuminated adequately by any method whatsoever.

It is the object of this paper to show from the properties of the dark-adapted eye what the colors of the markings and of the cockpit illuminant should be, and also to indicate the few simple precautions which have to be taken in the layout of the cockpit and the positioning of the lamps to insure good results. The layout of the cockpit is considered to be by far the most important single factor in cockpit lighting, but for convenience the color of the markings will be considered first.

Color of Markings

It is common knowledge that the ordinary green self-luminous markings cannot be focussed sharply by the normal dark-adapted eye at the distances at which these markings are observed, that is, about 18 inches or more. If the color is changed to orange without altering the brightness of either the markings or their background, it will be found that the markings stand out with a distinctness most surprising to those who for years have put up with the ordinary "fuzzy" green markings. Unfortunately orange self-luminous compounds of sufficient brightness have not been available until quite recently, and little is known as yet of their durability in service. Red self-luminous compounds of sufficient brightness are not available at the present time, and there are reasons for believing that they never will be.

Important as distinctness of markings undoubtedly is, there is still a stronger reason for changing from green to red or orange. In the dark-adapted eye the ratio of parafoveal to foveal sensitivity decreases from about 500 in the blue and 200 in the green, to about 10 in the yellow–orange and 0.8 in the red. When the pilot is looking out of the cockpit, the images of the markings are thrown on the parafovea, that is, are seen in the tail of the eye, and it is this parafoveal sensitivity ratio which determines the distracting effect of the cockpit lighting. In other words, what is wanted are markings of such a color that they appear to come up bright when the observer looks directly at them, but fade out as soon as the observer looks away from them. Deep red meets this requirement very closely, but even orange–red is a very great improvement on blue–green.

The reduction in the distracting effect of the markings is the fundamental reason for changing the color from green to orange, and for selecting red as the color of the general lighting, but there are other important reasons:

(*a*). As the specular reflectivity of the sidescreens is of the order of ten per cent, it follows that reflections of objects inside the cockpit cannot be seen in the sidescreens either foveally or parafoveally if the general illumination of the cockpit is such that the brightness of these objects is less than about ten times threshold. If white were used, these reflections seen parafoveally would appear to have a brightness about the same as the actual objects seen foveally.

(*b*). Because of the relative insensitivity of the parafovea to red, there is little or no danger of scattered light from the cockpit giving away the position of the aircraft to an enemy.

(*c*). The time taken for the eye to pass from the light to the dark-adapted state is very considerably shortened if adaptation takes place in the presence of weak red light instead of in complete darkness. If dark adaptation is lost due to the aircraft being caught in searchlights or through the use of a hand torch in the cockpit, then recovery of dark adaptation is hastened.

(*d*). Recent research indicates that in the presence of weak red light the final state of dark adaptation is not impaired to an appreciable degree, and, indeed, may be slightly better than in complete darkness.

As the last of the afore-mentioned reasons may be questioned, it is worthy of note that a similar result was obtained quite independently in tests using a typical single-seat cockpit fitted with instruments having yellow painted markings and illuminated with weak red light. It was found that after observing these markings, representative targets outside the cockpit could be picked up in times which were about ten per cent shorter

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than were obtained in complete darkness. This result seems to be explainable only on the assumption that the eyes are slightly more sensitive in weak red light.

Layout of Cockpit

All modern operational aircraft have totally enclosed cockpits. The enclosing surfaces must be polished in order to be transparent, and there is no practicable method of preventing these polished surfaces from reflecting whatever objects happen to bear the correct geometrical relationship with the observer's eye. It follows that reflections can be eliminated only by adopting the principle of the reflectionless shop window, that is, by arranging that the important surfaces, namely, the windscreen and those parts of the sidescreens immediately adjacent to it, shall reflect some surface of low brightness, that is, which is painted mat black. This black surface obviously must take the form of a coaming underneath the windscreen, as shown in Figure 1. In new designs such a coaming can be incorporated without detriment to the general layout, and even in existing types of British aircraft no case yet has been found in which a substantial coaming could not be fitted.

As regards the positioning of the lamps, the chief trouble is to avoid reflections of the light source itself in the cover glasses of the instruments on the panel. It follows from the geometry of the layout shown in Figure 1 that the pilot will not see images of the light source if the lamps are mounted as high or higher than the top of the upper row of instruments, and that the correct place to mount the lamps is underneath the coaming. The coaming therefore serves to prevent reflections both in the windscreen and in the cover glasses of the instruments. Where ultraviolet radiation is used the coaming has a further purpose.

It is obvious that the whole inside of the cockpit should be mat black and that the pilot's clothing, and particularly his gloves, also should be black. Instruments should not have protruding bezels or knobs which would cut off the light from other instruments. Markings should be bold, all of one standard size, and should be shaped and proportioned to give maximum legibility. Instruments with markings smaller than standard, if possible, should be grouped nearer the lamps than those with the larger markings. The instruments at the sides and bottom of the panel should be tilted so that neither the pilot's view of them nor the illumination incident on them is unduly oblique.

Methods of Cockpit Lighting

If it is assumed that the cockpit will be laid out and the lamps mounted as described previously, then the simplest solution would be to have the markings in orange self-luminous paint and to use red general cockpit lighting capable of being dimmed to extinction. It easily can be shown that markings which are just bright enough to be seen in complete darkness are bright enough to be seen on moonlit nights and in twilight. In any case, the general lighting system, if properly installed, can be used to boost the brightness of the markings if abnormal conditions arise, as for instance, when the aircraft is caught in searchlights. This paint is already used in a few British instruments, notably the lubber lines on the magnetic compass.

However, even if a satisfactory orange self-luminous paint should be forthcoming, there are objections to it on production and maintenance grounds, and therefore it was decided not to use it in the new British system of cockpit lighting. Red light on plain white painted markings was tried, but it was found that the markings appeared flat, owing partly to the deep-red color and partly to the fact that the contrast between the markings and their background in practice seldom exceeds 20 to 1. Also, most existing cockpits contain small miscellaneous objects which are light colored or give small specular reflections. For these reasons it was decided to use orange fluorescent markings and to supplement the red lighting with ultraviolet radiation, thereby obtaining the desired distinctness. This combination also permits both red and ultraviolet to be used at low intensities, thereby reducing troubles from specular reflections due to the red, or from eyeball fluorescence due to the ultraviolet. Also, either system acts as a standby for the other in case of lamp failures. This whole system is known as the dual system and has been adopted as the standard method of cockpit lighting for all operational types of British aircraft.

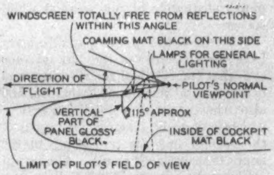
WINDSCREEN TOTALLY FREE FROM REFLECTIONS WITHIN THIS ANGLE
COAMING MAT BLACK ON THIS SIDE
LAMPS FOR GENERAL LIGHTING
DIRECTION OF FLIGHT
PILOT'S NORMAL VIEWPOINT
VERTICAL PART OF PANEL GLOSSY BLACK
115° APPROX
INSIDE OF COCKPIT MAT BLACK
LIMIT OF PILOT'S FIELD OF VIEW

Figure 1. Antireflection coaming

Reflected rays and image of coaming in windscreen shown dotted

Another method of rendering the instrument markings legible is to build lamps into each instrument, or light them from behind the panel as in automobiles. Unfortunately aircraft instruments, being of all shapes, sizes, and types, do not lend themselves well to being lighted in this way, and great difficulty is experienced in securing uniform dimming and uniform dial brightness, and in preventing light from leaking out round the edges of the bezels. These leakages can be overcome by the use of ultraviolet in some form, but the great disadvantages of the method as regards production and maintenance still remain, these disadvantages being even more marked in the case of British aircraft which have not hitherto had earth return. In any case, this method has to be used in conjunction with a floodlighting system in order to illuminate the large amount of gear which, in modern aircraft, lines the sides of the cockpit.

Fittings

The chief requirement for the fittings is to keep the size down. The red lamps used in the dual system are 2.4 inches long by 0.75 inch in diameter and accommodate a lamp of three watts. Two of these under the coaming usually can be arranged to flood the whole cockpit, that is, the instrument panel and the two sides. A local light is always required over the magnetic compass, but, since the needle, grid wires, and lubber line are orange self-luminous, this light is only used when setting a new course.

Ultraviolet radiation, if allowed to enter the eye, produces the effect of a blue mist, but in the dual system this is prevented partly by the coaming and partly by screening the lamps. As only a small amount of ultraviolet can be used without detriment to vision, the radiation should be obtained from tungsten filament lamps so as to obviate the difficulties associated with gaseous-discharge tubes, that is, dimming and radio interference. In the dual system two seven-watt fittings have been found sufficient for single-seat cockpits and four for two-seat cockpits.

Since, in the dual system all lights would go out if the aircraft electric supply failed, one additional orange lamp is fitted over the flying instrument panel, and is supplied from a separate alkaline accumulator. The members of the air crew also are issued with hand torches which may be fitted with red bulbs.

Concluding Remarks

The dual system now has been fitted to many types of aircraft and tested by many pilots, and in no case has any report adverse in principle been made.

So many conflicting requirements have to be met in the layout of a cockpit that an entirely happy solution to the lighting problem is not always possible, but there is no doubt that the aforementioned principles, if borne in mind in the early stages of the design and applied with ordinary ingenuity, will lead to reasonably satisfactory installations.

Considerations in Servomechanism Design

S. W. HERWALD
NONMEMBER AIEE

Synopsis: A servomechanism is in general any closed-cycle regulated system that is controlled by a difference of quantities. In this paper, methods are outlined by means of which the steady-state and transient performance of servomechanisms can be calculated. A general solution in operational form is given, and specific solutions are derived for a number of different types of angular-position servomechanisms. Among these is the solution for the Ward-Leonard type of control with *RC* antihunt feedback. This is probably the most common type of angular-position control.

SERVOMECHANISMS include many familiar regulator systems. The simple float valve controlling water level in a tank and the voltage regulator in reality are servomechanisms. The war has brought about many important new applications including: holding airplanes and ships on predetermined courses; stabilizing and rotating guns; producing exact duplicates of intricate patterns and dies; indicating flap or landing-gear position; and a multitude of others. Probably the most common servomechanism system is that which matches the position of a high-torque shaft to that of a low-torque shaft. This will be referred to as an angular-position servomechanism. By means of this servomechanism the high power required to operate a gun turret can be controlled so that the gun accurately matches the position of a low-power hand-operated sight. Whether the operating power is electric, hydraulic, or a combination of these or other types of power, the analysis to follow provides a basis for obtaining servomechanism performance in terms of basic design parameters.

Definition of a Servomechanism

In Figure 1, the nature of the component parts involved in a servomechanism and the meaning of the term closed-cycle are clearly illustrated. The three fundamental parts are the error measurer, the controller, and the output-quantity control member. As shown by the interconnecting arrows each of these basic parts is dependent for its operation upon one of the others and in turn influences the operation of the remaining part, hence the use of the term closed-cycle as shown by the closed counterclockwise system.

As previously defined, a servomechanism is in general any closed-cycle regulated system that is controlled by a difference of quantities. The wide scope of this definition can be realized best when it is noted that no limitation is placed on either the input or output quantities. Thus we may have regulation of speed, voltage, position, flow, and so forth. If the error measurer contains an element for converting quantities, the input and output quantities need not be the same. Many servomechanism problems have been solved commercially by Rototrol[11] or Amplidyne control or regulating systems. The basic component of each of these systems is rotating d-c equipment to which suitable control, either of the electronic or some other type, has been added.

The General Servomechanism

Figure 1 illustrates the basic elements of a general servomechanism and their interrelation. A theoretically perfect servomechanism would be one in which the output quantity varied with time exactly as the input. Referring to Figure 1 it is seen that this is not possible, since no error would be developed by which the output member could be controlled. The design of servomechanisms deals with methods of keeping both the magnitude and transient duration of the error between input and output small enough to meet the desired operating conditions.

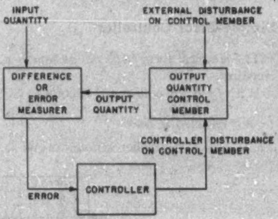
INPUT QUANTITY
EXTERNAL DISTURBANCE ON CONTROL MEMBER
DIFFERENCE OR ERROR MEASURER
OUTPUT QUANTITY
OUTPUT QUANTITY CONTROL MEMBER
CONTROLLER ON CONTROL
DISTURBANCE MEMBER
ERROR
CONTROLLER

Figure 1. General servomechanism block diagram

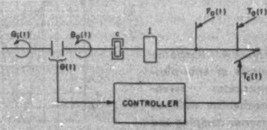

Figure 2. Angular-position servomechanism

The various elements in Figure 1, may be represented mathematically as time functions as follows:

$i(t)$ = input measurement as a function of time
$O(t)$ = output measurement as a function of time
$E(t)$ = error measurement as a function of time
$C(t)$ = control of output member as a function of time
$C_o(t)$ = external disturbance of output control member as a function of time

Also:

$$E(t)=i(t)-O(t) \tag{1}$$

The Laplacian operator form of any time function is given by

$$\phi(p)=p\int_0^\infty e^{-pt}\phi(t)dt \tag{2}$$

If $\phi(t)$, $\phi'(t)$, $\phi''(t)$, and so on $=0$ at $t=o$, p may be thought of as equivalent to $\frac{d}{dt}$. When equation 2 is used, equation 1 becomes

$$E(p)=i(p)-O(p) \tag{3}$$

Also:

$$C(p)=f_c(p)E(p) \tag{4}$$

$$O(p)=f_o(p)[C(p)+C_o(p)] \tag{5}$$

where

$f_c(p)$ is the response function of the controller.
$f_o(p)$ is the response function of the output control member.

Both of the above quantities are similar to the response function of an electric network. In the familiar response of an LR series circuit, $\frac{1}{R+LP}$ corresponds to $f_c(p)$ of equation 4, and $C(p)$ and $E(p)$ are analogous to the current and impressed voltage, respectively.

By substituting equations 3 and 4 into equation 5 and solving for $E(p)$, one obtains

$$E(p)=\frac{i(p)-f_o(p)C_o(p)}{1+f_o(p)f_c(p)} \tag{6}$$

This is the basic servomechanism equation and is of the type attributed to an unpublished memorandum of J. Taplin in 1937.[1] Taplin recognized the similarity of the automatic-control problem and the amplifier-design problem as given by Black[2] and Nyquist.[3] Integrating $E(p)$ in the Bromwich Wagner integral yields $E(t)$.

Over a period of years a number of solutions of equation 6 have been made. Certain assumptions and approximations

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Some of the material for this paper has been abstracted from a thesis submitted to the University of Pittsburgh in partial fulfillment of the requirements for the degree of doctor of philosophy.

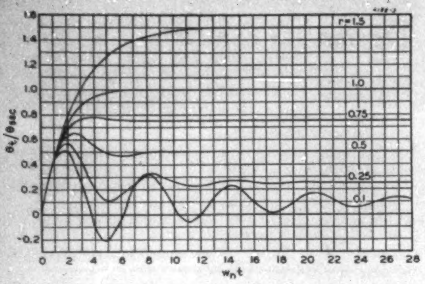

Figure 3. Response curves of simple-error servomechanism

were used to obtain these solutions. If these same assumptions and approximations can be justified by the designer, the established equations may be used, and the need for obtaining a new solution of equation 6 will have been eliminated. Once the characteristic equations of any given type of servomechanism have been obtained, they may be applied to any servomechanism of that type regardless of what the controlled parameter is. These equations permit analysis from which it is possible to design equipment to meet certain specifications. Stability as well as transient and steady-state errors can be predicted.

A good method to follow in the design of a servomechanism is to separate the mechanism into its component parts as shown by Figure 1, then to calculate the response functions of these components, and finally either to use an established solution of equation 6 such as those following or to obtain a new solution.

Angular-Position Servomechanism

The procedure to be followed in making an analysis of a servomechanism can be illustrated by consideration of the relatively common angular-position servomechanism. Such a system is schematically illustrated in Figure 2.

Where

$\theta_i(t)$ = input angle as a function of time
$\theta_o(t)$ = output angle as a function of time
$\theta(t) = \theta_i(t) - \theta_o(t)$ = error angle as a function of time
c = viscous damping coefficient of output member
I = lumped inertia of output member
$F_o(t)$ = coulomb friction as a function of time
$T_o(t)$ = external load torque as a function of time
$T_c(t)$ = controller torque as a function of time
$w_i(t)$ = input velocity as a function of time

The response of the output member can best be obtained by writing its differential equation:

$$\frac{Id^2\theta_o}{dt}+c\frac{d\theta_o}{dt}+T_o(t)+F_o(t)=T_c(t) \tag{7}$$

At $t=0$ with $\frac{d\theta_o}{dt}$ and $o=0_a$ the Laplacian form of the foregoing solved for $\theta_o(p)$ is

$$\theta_o(p)=\frac{1}{Ip^2+cp}\left(T_c(p)-T_o(p)-F_o(p)\right) \tag{8}$$

Comparing equation 8 and equation 5 one finds

$$f_o(p)=\frac{1}{Ip^2+cp}$$
$$C(p)=T_c(p)$$
$$C_o(p)=-T_o(p)-F_o(p)$$

By substituting into equation 6, one obtains

$$\theta(p)=\frac{(Ip^2+cp)\theta_i(p)+T_o(p)+F_o(p)}{Ip^2+cp+f_c(p)} \tag{9}$$

Since $p\theta_i(p)=w_i(p)$, equation 9 can be written as

$$\theta(p)=\frac{(Ip+c)w_i(p)+T_o(p)+F_o(p)}{Ip^2+cp+f_c(p)} \tag{10}$$

Thus once a control operator, $f_c(p)$, is determined, we have an error equation for either an angular-displacement or an angular-velocity input disturbance. Several solutions of equation 10 with different control operators, $f_c(p)$, are given.

Simple-Error Controller

The simplest form $f_c(p)$ can assume is a constant.

With $f_c(p)=K$ equation 4 becomes

$$T_c(p)=K\theta(p) \tag{11}$$

This gives a controller whose torque is proportional to the error angle, and K is known as the stiffness constant. If $f_c(p)=K$, $T_o(p)=F_o(p)=0$, and $w_i(p)$ is made w_1 at $t=0$, equation 10 becomes

$$\theta(p)=\frac{(Ip+c)w}{Ip^2+cp+K} \tag{12}$$

The effects of $T_o(p)$ and $F_o(p) \neq 0$ can be obtained by superposition.

Setting $p=0$ in equation 12 gives the steady-state error

$$\theta_{ss}=\frac{cw_1}{K} \tag{13}$$

With the effects of $T_o(t)$ and $F_o(t)$ considered

$$O_{ss}=\frac{cw_1+T_o+F_o}{K} \tag{14}$$

Similarly in equation 9 with $\theta_i(p)=\theta_1$

$$\theta_{ss}=\frac{T_o+F_o}{K} \tag{15}$$

In determining stability and transient response of a servomechanism, it is convenient to use some standard input function and then to compare the errors obtained using different controller parameters. A very useful and practical input function of that type is constant velocity, w_1, applied to the input at $t=0$ and held at that value thereafter. This is the case described by equation 12 for the simple-error type of controller.. Rewriting equation 12 in terms of $r=\frac{c}{c_c}=\frac{c}{2\sqrt{IK}}$, the ratio of actual to critical damping, and $w_n=\sqrt{\frac{K}{I}}$ the undamped natural frequency of the system yields a more general solution. Then

$$\theta(p)=\frac{(p+2rw_n)w_1}{p+2rw_np+w_n^2} \tag{16}$$

The solutions for equation 16 obtained by operational methods are

$r<1$

$$\frac{\theta(t)}{\theta_{ssc}}=r\left(1-e^{-rw_nt}\left(\cos\sqrt{1-r^2}\,w_nt+\frac{2r^2-1}{2r\sqrt{1-r^2}}\sin\sqrt{1-r^2w_nt}\right)\right) \tag{17}$$

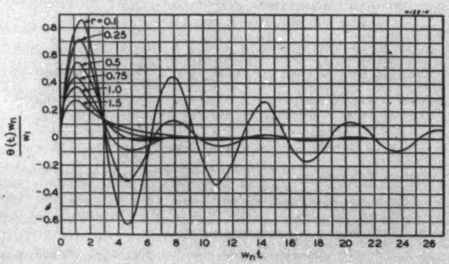

Figure 4. Response curves of error-plus-error-rates servomechanism with no viscous damping and (l-a) held constant

$r=1$

$$\frac{\theta(t)}{\theta_{ssc}}=1-e^{-w_nt}\left(1+\frac{w_nt}{2}\right) \qquad (18)$$

$r>1$

$$\frac{\theta(t)}{\theta_{ssc}}=r\left(1-e^{rw_nt}\left(\cosh\sqrt{r^2-1}w_nt+\frac{2r^21}{2r\sqrt{r^2-1}}\sinh\sqrt{r^2-1}w_nt\right)\right) \qquad (19)$$

θ_{ssc} is the steady-state error with critical damping and is equal to $2w_1/w_n$. $\theta_{ssc}r=2rw_1/w_n$ and checks θ_{ss} in equation 13. Figure 3 gives $\theta(t)/\theta_{ssc}$ as the ordinate and w_nt as the abscissa, for different values of r. Since both $\theta(t)/\theta_{ssc}$ and w_n are dimensionless and neither contains r, the curves shown in Figure 3 give both the relative magnitude and duration of the transient errors. Figure 3 is similar to the curves given by Hazen,[4] with the exception that different dimensionless parameters which give a truer picture of transient duration were used. As expected Figure 3 shows that when the damping ratio, r, is increased, the oscillation about the steady-state position is reduced, although the time initially to approach the steady-state error is increased. If a fairly large initial overshoot can be tolerated, small damping ratios may give satisfactory performance because of their correspondingly lower steady-state errors.

The prime difficulty with a regulator of this type occurs because the damping torque is proportional to speed. Consequently, at high operating speeds, there is a large amount of power being dissipated in the damper. This loss can be tolerated in low-power systems; however, high-power systems require other means of obtaining stable operation. As is usual in design, compromise is necessary. High values of r are desired to prevent large transient overshoot, yet these same values bring about an increase in the steady-state error and damping power loss.

Error-Plus-Error-Rates Controller

Examination of equation 10 shows that to overcome the disadvantages of the simple-error controller a positive term in p and a negative term in p^2 are desired in the denominator. The positive term will provide damping to the system without increasing the steady-state error or damping loss for a constant-velocity input, since both are dependent only on c. The negative term in p^2 will minimize inertia effects. Such a controller would give $f(p)$ in equation 10 the form,

$$f_c(p)=-ap^2+bp+K \qquad (20)$$

and

$$T_c(t)=-a\frac{d^2\theta}{dt^2}+b\frac{d\theta}{dt}+K\theta \qquad (21)$$

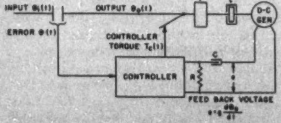

Figure 5. Schematic representation of error-plus-RC-output-feedback servomechanism

If equation 1 and equation 7 are used, the differential torque equation can be written as

$$I\frac{d^2\theta_i}{dt^2}+c\frac{d\theta_i}{dt}+T_o(t)+F_o(t)=(I-a)\frac{d^2\theta}{dt}+(b+c)\frac{d\theta}{dt}+K\theta \qquad (22)$$

Rewritten in operational form

$$\theta(p)=\frac{(mp^2+2frw_np)\theta_i(p)+(1-m)pw_1+T_o(p)+F_o(p)}{p^2+2rw_np+w_n^2} \qquad (23)$$

with the initial boundary values, $(1-m)pw_1$ showing up as an external torque and where

$$m=\frac{I}{I-a},\quad f=\frac{c}{b+c},\quad r=\frac{b+c}{2\sqrt{(I-a)K}},$$

$$w_n=\sqrt{\frac{K}{I-a}}$$

With $p\theta_i(p)=w_1$ and $T_o(p)=F_o(p)=0$ equation 23 becomes

$$\theta(p)=\frac{(p+2frw_n)w_1}{p^2+2rw_np+w_n^2} \qquad (24)$$

Solutions by operational methods give

$r<1$

$$\frac{\theta(t)}{\theta_{ssc}}=r\left(1-e^{-rw_nt}\left(\cos\sqrt{1-r^2}w_nt+\frac{2fr^2-1}{2fr\sqrt{1-r^2}}\sin\sqrt{1-r^2}w_nt\right)\right) \qquad (25)$$

$r=1$

$$\frac{\theta(t)}{\theta_{ssc}}=1-e^{-w_nt}\left(1+w_nt-\frac{w_nt}{2f}\right) \qquad (26)$$

$r>1$

$$\frac{\theta(t)}{\theta_{ssc}}=r\left(1-e^{-rw_nt}\left(\cosh\sqrt{r^2-1}w_nt+\frac{2fr^2-1}{2fr\sqrt{r^2-1}}\sinh\sqrt{r^2-1}w_nt\right)\right) \qquad (27)$$

Where

$$\theta_{ssc}=\frac{2fw_1}{w_n}$$

Most designers strive to make c as small as possible so that steady-state error for constant velocity input is minimized. The solutions of equation 24 with $c=f=0$ are

$r<1$

$$\frac{\theta(t)w_n}{w_1}=\frac{e^{-rw_nt}}{\sqrt{1-r^2}}\sin\sqrt{1-r^2}w_nt \qquad (28)$$

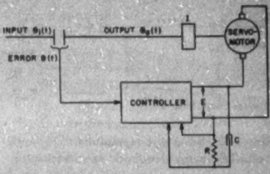

Figure 6. Schematic representation of error-plus-negative-RC-feedback servomechanism

$r=1$

$$\frac{\theta(t)w_n}{w_1}=w_nte^{-w_nt} \qquad (29)$$

$r>1$

$$\frac{\theta(t)w_n}{w_1}=\frac{e^{-rw_nt}}{\sqrt{r^2-1}}\sinh\sqrt{r^2-1}w_nt \qquad (30)$$

The foregoing equations are particularly useful because they result in simple transient solutions to which the steady-state error for $f\rightarrow 0$ can be added without any appreciable inaccuracy. Figure 4 is a plot of $\frac{\theta(t)w_n}{w_1}$ against w_nt for different values of r with $(I-a)$ held constant. It should be noted carefully that for stability $a<I$, since the first criterion of stability is that all coefficients be positive. Again as in Figure 3, the abscissa, w_nt, does not contain r so that it truly represents transient durations for variations of r. Figure 4 illustrates quite clearly the compromise involved in derivative damping:

(a). Transient duration may be decreased at the expense of peak error.

(b). Transient peaks may be decreased by providing a controller capable of delivering high values of derivative damping. This usually involves a more elaborate controller.

As difficult as it is to obtain high value of first derivative damping reliably, the problem is extremely more difficult in obtaining a component proportional to p^2. Usually one takes the values of a that can be obtained and has little control over its magnitude.

Error Controller With RC-Output Feedback

The difficulty of obtaining economically quantities accurately proportional to error change has led to a wide practice of using a resistor-capacitor network across a voltage proportional to the output speed, $d\theta_o/dt$, to obtain antihunt. A common system is to use a d-c tachometer generator directly connected to the output drive. A schematic diagram of a system of this type is shown in Figure 5. Neglecting load and friction torque, torques may be equated as follows:

$$I\frac{d^2\theta_o}{dt^2}+c\frac{d\theta_o}{dt}=T_c(t) \qquad (31)$$

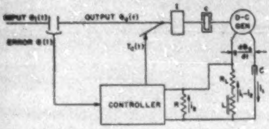

Figure 7. Schematic representation of error-plus-*LRC*-output-feedback servomechanism

Two sets of initial conditions have to be assumed, because different transient responses are obtained when the output velocity function $p\theta_i(p)$ is taken as an acceleration or deceleration. This is caused by the charge on the capacitor, C, for a steady-state velocity The two sets of conditions assumed are:

A. At $t=0$

$$\theta=\theta_i=\theta_o=0$$

$$\frac{d\theta}{dt}=\frac{d\theta_i}{dt}=\frac{d\theta_o}{dt}=0$$

$$\left(\frac{1}{C}\int idt\right)_{t=0}=0 \tag{32}$$

B. At $t=0$

$$\frac{d\theta}{dt}=\theta_o=0$$

$$\theta_i=\theta=\frac{cw_1}{K}$$

$$\frac{d\theta_i}{dt}=\frac{d\theta_o}{dt}=w_1$$

$$\left(\frac{1}{C}\int idt\right)_{t=0}=gw_1 \tag{33}$$

The solutions for $\theta(p)$ using boundary conditions (equations 32 and 33) are identical with the exception that the initial values given by equation 33 show up as an external torque. $C_0(p)$ in equation 6. Thus as derived in equations 56 and 60.

$$O(p)=\theta_o(p)$$

$$f_o(p)=\frac{1}{Ip^2+cp+\dfrac{K_2RCgp^2}{1+RCp}}$$

$$C(p)=K\theta(p) \tag{34}$$

$$E(p)=\theta(p)$$

$$f_c(p)=K$$

$C_o(p)=0$ using equation 32

$C_o(p)=\left(I+\dfrac{K_2RCg}{1+RCp}\right)pw_1$ using equation 33

The solution for $\theta(p)$, using equation 32, is given by equation 57 as

$$\theta(p)=\frac{\left(Ip^2+cp+\dfrac{K_2RCgp^2}{1+RCp}\right)\theta_i}{Ip+cp+\dfrac{K_2RCgp^2}{1+RCp}+K}(p) \tag{35}$$

Letting $p\theta_i(p)=w_1$ at $t=0$

$$\theta(p)=\frac{\left(p^2+hp+\dfrac{c}{RCI}\right)w_1}{p^3+kp^2+\left(\dfrac{c}{RCI}+\dfrac{K}{I}\right)p+\dfrac{K}{RCI}} \tag{36}$$

where

g = generator constant, volts per radian per second

K_2 = controller constant, foot-pound per volt

$$h=\frac{1}{RC}+\frac{K_2g}{I}+\frac{c}{I}$$

The corresponding solution for $\theta(p)$ using equation 33 is given by equation 61 as

$$\theta(p)=\frac{\left(Ip^2+cp+\dfrac{K_2RCgp^2}{1+RCp}\right)\theta_i(p)-\left(I+\dfrac{K_2RCg}{1+RCp}\right)pw_1}{Ip^2+cp+\dfrac{K_2RCgp^2}{1+RCp}+K} \tag{37}$$

If at $t=0$, $p\theta_i(p)$ is instantaneously made zero, $\theta_i(p)$ remains at the value cw_1/K

and

$$\theta(p)=\frac{\left(p^3+hp^2+\dfrac{c}{RCI}p\right)\dfrac{cw_1}{K}-\left(p+h-\dfrac{c}{I}\right)pw_1}{p^3+hp^2+\left(\dfrac{c}{RCI}+\dfrac{K}{I}\right)p+\dfrac{K}{RCI}} \tag{38}$$

The steady-state error for constant velocity input, w_1, is still cw_1/K as it was for the simple-error case. To reduce this error, c is made as small as possible. The case with $c=0$ is of importance and reduces both equations 36 and 38 to the same value with the exception of sign. With the positive sign

$$\theta(p)=\frac{\left(p+\dfrac{1}{RC}+\dfrac{K_2g}{I}\right)pw_1}{p^3+\left(\dfrac{1}{RC}+\dfrac{K_2g}{I}\right)p^2+\dfrac{K}{I}p+\dfrac{K}{RCI}} \tag{39}$$

For $\theta(t)$ to be damped the product of the second and third denominator term coefficients must exceed the first and fourth term coefficient product. This requires $K_2RCg>0$ which is a result that easily could have been anticipated. Solutions for $\theta(t)$ from equations 36, 38 and 39 may be obtained by general methods. However, in many cases, it is easier to assign specific values to the constants, find the roots of the equation by a method with which one is most familiar, and obtain several particular solutions by operational methods, rather than a general solution. If c arbitrarily is made zero only in the denominator of equations 36 and 38, equations result which contain a steady-state velocity error yet do not derive any damping from that energy loss. This represents the case of a d-c motor with thyratron armature control. Here we have a velocity error, because a definite armature voltage must be maintained for constant velocity, and yet no decelerating damping because of the rectifier characteristic of the thyratrons.[5,6]

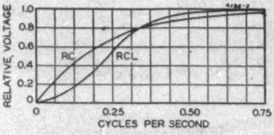

Figure 8. Absolute voltage comparison of *RC* and *LRC* antihunt circuits

$R=200{,}000$ $R_L=100{,}000$
$C=3$ microfarads $L=20{,}000$ henrys

The big disadvantage of using *RC* output damping is that it retards acceleration response.

$\theta(p)$ for Error Controller With Negative RC Feedback

Typical of the system outlined in Figure 6, is a Ward-Leonard control system with the generator included in the controller. E of Figure 6 then corresponds to the variable voltage of the Ward-Leonard system. The well-known equation for armature current in a d-c motor with constant excitation is

$$i_a=\frac{E-g\dfrac{d\theta_o}{dt}}{Ra}$$

With load torque neglected the accelerating torque is proportional to i_a or

$$C_1i_a=\frac{EC_1-gC_1\dfrac{d\theta_o}{dt}}{Ra}=I\frac{d^2\theta_o}{dt^2} \tag{40}$$

By following the procedure outlined in the Appendix, and using conditions in equations 32 and 33 again, one finds

$$f_o(p)=\frac{1}{Ip^2+cp}$$

$$C(p)=\frac{K(1+RCp)\theta(p)}{1+RC(1+K_2)p} \tag{41}$$

$$f_c(p)=\frac{K(1+RCp)}{1+RC(1+K_2)p}$$

$C_o(p)=0$ using equation 32

$C_o(p)=\left(I+\dfrac{cK_2RC}{1+RC(1+K_2)p}\right)pw_1$ using equation 33

Use of conditions in equation 32, substitution in equation 6 yields

$$\theta(p)=\frac{(Ip^2+cp)\theta_i(p)}{Ip^2+cp+\dfrac{K(1+RCp)}{1+RC(1+K_2)p}}$$

Letting $p\theta_i(p)=w_1$ at $t=0$

$$\theta(p)=\frac{\left(p^2+\left(h_1+\dfrac{c}{I}\right)p+\dfrac{h_1c}{I}\right)w_1}{p^3+\left(h_1+\dfrac{c}{I}\right)p^2+\left(\dfrac{RCK}{I}+\dfrac{c}{I}\right)h_1p+\dfrac{Kh_1}{I}} \tag{42}$$

where

$$h_1=\frac{1}{RC(1+K_2)}$$

Use of conditions in equation 33 and substitution in equation 6 yield

$$\theta(p)=\frac{(Ip^2+cp)\theta_i(p)-\left(I+\frac{(cK_2RC)}{1+RC(1+K_2)p}\right)pw_1}{Ip^2+cp+\frac{K(1+RCp)}{1+RC(1+K_2)p}} \tag{43}$$

If at $t=0$, $p\theta_i(p)$ is instantaneously made zero, $\theta_i(p)$ remains at the value $\frac{cw_1}{K}$ and

$$\theta(p)=\frac{\left[\left(p^3+\left(h_1+\frac{c}{I}\right)p^2+\frac{h_1cp}{I}\right)\frac{cw_1}{K}-\left(p+h_1+\frac{cK_2}{I(1+K_2)}\right)pw_1\right]}{p^3+\left(h_1+\frac{c}{I}\right)p^2+\left(\frac{RCK}{I}+\frac{c}{I}\right)h_1p+\frac{Kh_1}{I}} \tag{44}$$

As was the case with output damping, $\theta(t)$ can be found by operational methods. This system of obtaining stability also is detrimental to acceleration response because it depends for its operation on slowing up the appearance of the voltage, E.

Error Controller With LRC Output Feedback

With a feedback circuit such as used in Figure 7, rather than one as used in Figure 5, the advantage of a faster acceleration response without sacrifice of antihunt is obtained. The simplest way to show what results is to plot the absolute value of the voltage appearing across R against frequency for both the RC and LRC cases. Figure 8 gives such a comparison. In the lower-frequency range the LRC circuit gives less negative feedback, consequently, acceleration rates are higher. However, in the higher-frequency ranges the negative feedback of both the LRC and RC circuits are about the same. By selecting the value of L, R, and C the frequency at which the feedback voltage first reaches its peak can be made the natural hunting frequency of the system. If this be done, the system is damped because of high negative feedback at hunting frequency, and yet acceleration response is not too badly affected because the amount of negative feedback falls off rapidly at frequencies below the hunting frequency. Using an inductance in this manner is quite similar to the method used to increase the gain of a capacitor-coupled amplifier at high frequencies. Figure 7 shows only one section of this type of high-pass filter. More than one section gives even a sharper change from low to high negative-feedback values than that shown in Figure 8. The solution of the LRC problem in operational form is obtained by following the same procedure outlined in the appendix. Then

$$f_o(p)=\frac{1}{Ip^2+cp+\frac{K_2RCg(R_L+Lp)p^2}{R+R_L+Lp+RCp(R_L+Lp)}} \tag{45}$$

$C(p)=K\theta(p)$

$f_c(p)=K$

$C_o(p)=0$ (using equation 32)

$$C(p)=\left(I+\frac{K_2RCgR_L+Lp}{R+R_L+Lp+RCp(R_L+Lp)}\right)pw_1$$

When equations 32 and 45 are substituted into equation 6 and one lets $p_i\theta(p)=w_1$ at $t=0$

$$\theta(p)=\frac{\left(Ip+c+\frac{K_2RCg(R_L+Lp)p}{R+R_L+Lp+RCp(R_L+Lp)}\right)w_1}{Ip^2+cp+\frac{K_2RCg(R_L+Lp)p^2}{R+R_L+Lp+RCp(R_L+Lp)}+K} \tag{46}$$

When equations 33 and 45 are used similarly and substituted into equation 6, then if at $t=0$ w_1 instantaneously is made zero, $\theta(p)$ remains at cw_1/K and

$$\theta(p)=\frac{\left(Ip^2+cp+\frac{K_2RCg(R_L+Lp)p^2}{R_L+R+Lp+RCp(R_L+Lp)}\right)\frac{cw_1}{K}-\left(I+\frac{K_2RCg(R_L+Lp)}{R+R_L+Lp+RCp(R_L+Lp)}\right)pw_1}{Ip^2+cp+\frac{K_2RCg(R_L+Lp)p^2}{R_L+R+Lp+RCp(R_L+Lp)}+K} \tag{47}$$

Time Delays

All the equations given so far have neglected time delays. In systems in which this delay time is small compared to the natural period, the equations can be used as given. However, when long relative delay times are involved, they must be introduced. In the familiar case of current response of a series LR circuit,

$$i=\frac{1}{1+\frac{L}{R}p}\cdot\frac{E}{R}$$

and L/R occurring in the denominator can be thought of as a time delay, t. The steady-state value, E/R, has already been included in the constants K and K_2 used in the response equations. Therefore, in order to introduce time delay the affected term merely should be multiplied by $\frac{1}{1+tp}$. If more than one time delay is to be included, by a similar line of reasoning the affected term is multiplied by

$$\frac{1}{(1+t_1p)(1+t_2p)\ldots(1+t_np)} \tag{48}$$

Thus for the Ward-Leonard system which contains both field and armature time delays, equation 35 becomes

$$\theta(p)=\frac{\left(Ip^2+cp+\frac{K_2RCgp^2}{(1+t_fp)(1+t_ap)(1+RCp)}\right)\theta_i(p)}{Ip^2+cp+\frac{K_2RCgp^2}{(1+t_fp)(1+t_ap)(1+RCp)}+\frac{K}{(1+t_fp)(1+t_ap)}}$$

Similarly equations including time delay could be written for all the response equations given. Rationalizing an equation of the type given in equation 49 results in a denominator containing a fifth degree equation in p. Finding the roots of that equation and then using operational methods gives the solution for $\theta(t)$. The increasing complexity of the solution for $\theta(p)$, when several time delays are included, has resulted in a practice of finding the roots of the denominator equation and evaluating the stability by the damping factors of the roots.[7,8]

Types of Input Functions

All the response equations given have used a unit function as the input. This is a simple function by which the response for the different systems of control can be directly compared. However, it is not the only one that can be used. If the response to a sine, constant acceleration, or any other input is desired, it is necessary merely to substitute the correct operational form of the sine or constant acceleration input for $\theta_i(p)$ and proceed with the solution exactly as if w_1 had been substituted for $p\theta_i(p)$. This provides a method for finding the exact response to a known input function. Most problems, however, involve random inputs, consequently a good method of comparison such as is provided by the unit function is adequate.

Summary

The previous example of a remotely located gun turret continuously following a hand-operated sight serves well to illustrate the method and equations given. In the design of such a system it is desired that the gun stay in line with the sight within close limits. It is also desired that the gun deviate as little as possible and realign itself as quickly as possible after some disturbance has moved the sight. Thus, the sight position corresponds to $\theta_i(t)$ and the difference in their respective positions at any time to $\theta(t)$. Finding $\theta(t)$ helps enormously in designing a control system, for not only is it the error equation but also it shows what values such fundamental parameters as the stiffness of the system, K, the viscous damping, c, the inertia, I, and the antihunt feedback constant, K_2, must have in order to keep errors within desired limits. The error equation in operational form $\theta(p)$, has been given for a number of different systems of control, any of which will do the job. Each system accomplishes the basic purpose of keeping the gun turret aligned with the sight. However, the transient and steady-state errors may be quite different for similar values of stiffness, damping, inertia, and so forth. It is the problem of the designer to evaluate the complexity of any system against its merit and then select the simplest system that will perform within desired limits. Setting p equal to zero in any of the derived equations for $\theta(p)$ gives steady-state errors for angular displacement and velocity. From these can be selected values of viscous damping and stiffness which will give a small enough steady-state error to meet a given specification. Integrating $\theta(p)$ operationally gives $\theta(t)$, the transient error response. From the resulting equation values of antihunt, K_2, or error rate of change, b, can be selected which will limit the peak error to the desired value.

The method shown for obtaining these criterion equations for $\theta(p)$, consists of breaking the servomechanism up into its component parts, obtaining the controller and output response functions, and then obtaining $\theta(p)$ by using the general servomechanism equation 6. This is a systematic procedure for handling an unwieldy problem. Further refinement can be made by introducing time delays and varied input functions. Although all particular solutions obtained by use of equation 6 were for angular position regulators, equation 6 is entirely general. Therefore servomechanisms to control such quantities as voltage, speed, pressure, and so forth are represented by equation 6. Particular solutions obtained for the angular position servomechanism can be applied to servomechanisms controlling other quantities by using unit analogy.

Appendix. $\theta(t)$ for Error Controller With RC Output Feedback

In order to calculate the feedback component of torque, it is first necessary to find the voltage appearing across R of Figure 5, as a function of time. The series voltage summation is

$$iR+\frac{1}{C}\int idt=g\frac{d\theta_0}{dt} \tag{49}$$

By standard operational methods this can be rewritten as

$$Ri(p)+\frac{i(p)}{Cp}+\left(\frac{1}{C}\int idt\right)_{t=0}=gp\theta_o(p)-gp(\theta_o)_{t=0} \tag{50}$$

When conditions from equation 32 are used, the voltage appearing across R, is

$$Ri(p)=\frac{RCgp^2\theta(p)}{1+RCp}$$

which, when fed back into the controller negatively, gives an output torque component

$$-K_2Ri(p)=-\frac{K_2RCgp^2\theta_o(p)}{1+RCp} \tag{51}$$

where K_2 is the controller constant with units of foot-pounds per volt. Then for a controller which also has a simple error component, the controller torque is

$$Tc(p)=K\theta(p)-\frac{K_2RCgp^2\theta(p)}{1+RCp} \tag{52}$$

With load, $T_o(t)$, and friction, $F_o(t)$, neglected, the differential torque equation is

$$I\frac{d^2\theta_o}{dt^2}+c\frac{d\theta_o}{dt}=T_e(t) \tag{53}$$

When operational methods and boundary conditions from equation 32 are used

$$Ip^2\theta_o(p)+cp\theta_o(p)=Tc(p)=K\theta(p)-\frac{K_2RCgp^2\theta_o(p)}{1+RCp} \tag{54}$$

This can be rewritten in the form,

$$\theta_o(p)=\left(\frac{1}{Ip^2+cp+\dfrac{K_2RCgp^2}{1+RCp}}\right)K\theta(p) \tag{55}$$

When equations 4 and 5 are used, the following comparisons can be made:

$$O(p)=\theta_o(p)$$

$$f_o(p)=\frac{1}{Ip^2+cp+\dfrac{K_2RCgp^2}{1+RCp}} \tag{56}$$

$$C(p)=K\theta(p)$$
$$E(p)=\theta(p)$$
$$f_c(p)=K$$

By using equations 6 and 56 and rationalizing, one obtains

$$\theta(p)=\frac{\left(Ip^2+cp+\dfrac{K_2RCgp^2}{1+RCp}\right)\theta_i(p)}{Ip^2+cp+\dfrac{K_2RCgp^2}{1+RCp}+K} \tag{57}$$

If the same procedure previously outlined is followed and conditions from equation 33 are used,

$$T_e(p)=K\theta(p)-\frac{K_2RCg(p^2\theta_o(p)-pw_1)}{1+RCp} \tag{58}$$

Use equation 56, boundary conditions of equation 33 and solve for $\theta(p)$:

$$\theta_o(p)=\left(\frac{1}{Ip^2+cp+\dfrac{K_2RCgp^2}{1+RCp}}\right)\times\left[K\theta_o(p)+\left(I+\frac{K_2RCg}{1+RCp}\right)pw_1\right] \tag{59}$$

Comparison with equation 5 gives

$$C_o(p)=\left(I+\frac{K_2RCg}{1+RCp}\right)pw_1 \tag{60}$$

in addition to the previous relations, which were established in equation 56. Thus the initial boundary conditions occur as external torques in the general equation. By using equations 6, 56, and 60 and rationalizing, we obtain

$$\theta(p)=\frac{\left(Ip^2+cp+\dfrac{K^2RCgp^2}{1+RCp}\right)\theta_i(p)-\left(I+\dfrac{K_2RCg}{1+RCp}\right)pw_1}{Ip^2+cp+\dfrac{K^2RCgp^2}{1+RCp}+K} \tag{61}$$

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Design Considerations of 400-Cycle Aircraft Motors

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SEVERAL papers have been presented describing the reason for the selection of 400-cycle three-phase service for motors aboard aircraft. In view of the fact that the decision has been made to use 400 cycles, the purpose of this paper is to present some of the motor-design problems to the men who must write the specifications for these motors. A better general understanding of the performance to be expected from 400-cycle induction motors will help both the aircraft electrical engineer and the motor designer.

An electric system such as used on an airplane must be designed as a unit. The generating system is never sufficiently large to permit the assumption of uniform voltage and frequency under all conditions. The need for dependability requires the use of some reliable overload-protection device which must be considered as part of the motor. The type of load, which may involve a large variation in torque during the duty cycle, must be completely understood by the motor designer. Also, the motor control must be such as to insure reliable motor operation. The need for co-operation between the engineers working on these varied but intimately related problems cannot be too highly emphasized.

Only those characteristics of 400-cycle motors which differ radically from characteristics of the 24-volt d-c motors used in the past will be discussed. Mechanical problems will not be discussed, although new mechanical problems have appeared due to the possibility of speeds up to 24,000 rpm.

First we will list the requirements of the airplane designer and then point out some of the problems faced by the motor designer to meet these requirements.

1. The first requirement of the airplane designer, so obvious that it is frequently overlooked or subordinated to other requirements, is dependability. The motor must do the job assigned to it unfalteringly on each occasion when it is needed during the period between servicing. In this respect the induction motor has definite obvious advantages over the 24-volt d-c motor. These include simpler windings, freedom from commutation, and fewer parts.

2. The weight must be minimum consistent with dependability.

3. The motor must be capable of doing its job when the maximum voltage is ten per cent above or ten per cent below rating, and when the frequency is five per cent above or below rating.

4. The requirements for starting torque in proportion to full-load torque will vary considerably, but should ordinarily not be below 120 per cent of full-load torque.

5. The maximum torque permissible is determined by the airplane structure and is sometimes as low as 120 per cent of the full-load torque.

6. If the motor is to run continuously, its efficiency may be as important as its weight because of the added weight of fuel which must be carried to run a less efficient motor.

7. It is desirable to have a high power factor to reduce line losses and generating capacity required.

8. The same ambient temperature, humidity, altitude, and vibration specifications apply to 400-cycle motors as were used with the d-c motors.

These, then, are the primary requirements of the airplane designer which affect the motor designer. The generalization that high speed means less weight per horsepower will now be investigated by considering the results of running a familiar one-horsepower 60-cycle four-pole induction motor from a 400-cycle power supply.

The motor selected is one having quite common performance characteristics. The starting torque is 200 per cent of full-load torque, and the maximum or breakdown torque is 300 per cent of full-load torque. The efficiency is 80 per cent and the slip at full load is four per cent. The motor being considered differs from an ordinary industrial motor in that better materials are used and precautions are taken to prevent interlamination losses. The motor is totally enclosed so that the analysis will not be confused too much by increased friction and windage. Otherwise, the motor is a conventional one with a well proportioned lamination and normal performance.

The full-load torque of any motor is the maximum torque which can be developed by the motor for the rated time of operation without exceeding a safe operating temperature. This is the case in all motors, although the interpretation of "safe operating temperature" will be revised drastically when the motor is used aboard an airplane. For purposes of comparison let us assume that the same cooling is used on 400 cycles as on 60 cycles, that the same temperature rise is permitted, and therefore the same total losses in the motor are permissible.

Let us now consider how this motor will operate on 400-cycle service. In order to keep the flux the same, the motor must be operated on a voltage 6.7 times higher than the 60-cycle voltage. That is, the voltage must be increased in proportion to frequency. The synchronous speed, of course, increases from 1,800 rpm to 12,000 rpm. The magnetizing current increases only slightly. The motor resistance is not changed at all by operating the motor on 400 cycles; however, the leakage reactance increases in proportion to the frequency, that is 6.7 times.

The rated torque is determined by the losses, and is practically the same as on 60 cycles. Therefore, the horsepower rating is increased by 6.7 times. The breakdown torque has increased by 70 per cent to over 500 per cent of full-load torque. This follows from the increase of voltage proportional to frequency, together with a much smaller increase in circuit impedance.

Since the motor produces the same full-load torque with the same losses, but at 6.7 times the speed, the efficiency has been increased considerably. The efficiency has come up from 80 per cent to 95 per cent or better.

The slip at full-load torque is reduced from four per cent to 0.6 per cent. This can be understood by picturing what happens from the standpoint of the rotor. The torque is produced by the action of the rotor current on the rotating flux produced by the field. By operating the motor on 400 cycles instead of 60 cycles, the magnitude of the flux is not altered; only the speed of the rotating flux has been changed. On 60 cycles, with a slip of four per cent or 72 rpm, the rotor bars were cutting the flux at such a speed as to produce sufficient rotor current to develop full-load torque. On 400 cycles the flux is rotating 12,000 rpm; however, a difference in speed of 72 rpm between the rotating field and the rotor still produces sufficient rotor current to develop full-load torque. The current in the rotor under load depends on the voltage induced in the bars, which in turn depends on the rate of cutting flux.

The slip at which maximum torque is obtained will also be very small. Maximum torque on 60 cycles is obtained at 30 per cent slip, the slip for maximum torque on 400 cycles will be near five per cent. The fact that all torques from zero to maximum are produced within a range of five per cent is useful in synchronizing actuators located at remote parts of the airplane.

So far we have presented a very satis-

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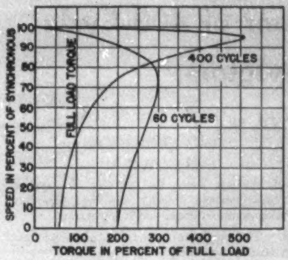

Figure 1. Speed–torque curves

factory picture of increased performance in horsepower per pound by increasing the frequency. One disturbing factor remains to be considered. This factor is the starting torque. A 60-cycle motor designed for maximum horsepower per pound has adequate starting torque. However, a 400-cycle motor designed for maximum efficiency and horsepower per pound has a relatively small starting torque. Redesign by changing the circuit constants of the 400-cycle motor results in a loss of efficiency and consequent increase in weight per horsepower.

The starting torque is proportional to the I^2R loss in the rotor divided by the synchronous speed. Returning to the example, the locked rotor current on 400 cycles is 40 per cent higher than the current on 60 cycles. The I^2R loss will approximately double, while the synchronous speed is increased 6.7 times; the torque, then, will be reduced 3.3 times. This means that instead of 200 per cent starting torque the motor now develops 60 per cent starting torque. In actual practice the rated full-load torque of the motor could be increased considerably by improving the cooling and by raising the allowable temperature limit. This increase in rating only serves to lower the percentage starting torque. This difficulty is a major problem in the design of 400-cycle motors for aircraft use.

This example has been purposely selected to show the difficulty clearly. This difficulty is not nearly so pronounced in very small motors because the leakage reactance is relatively small in comparison to the resistance, and the majority of motors used aboard aircraft are small. The problem remains in some degree, however, and must be overcome. Figure 1 illustrates the example. Speeds in percentage of synchronous are plotted as ordinates and torques are plotted as abscissas. The motor on 400 cycles produces the same torque at rated horsepower as on 60 cycles, but produces much less torque at standstill.

We shall now consider ways of increasing this usually inadequate starting torque. As previously stated, the starting torque varies directly as the I^2R loss in the rotor and inversely as the synchronous speed. To increase the torque, the rotor current or the rotor resistance, or both, must be increased. Table I shows the comparative values obtained in operating our one-horsepower motor on 60 cycles and on 400 cycles, together with the effect of increasing the rotor resistance. In many aircraft applications it may be desirable to produce the high maximum torque at standstill. In the example chosen this would be accomplished by increasing the rotor resistance about 20 times. The penalty, however, would lie in the necessary decrease in rated torque due to the heating in the high-resistance rotor. The reduction in rated torque would be by a factor of approximately three. With the starting torque 500 per cent of the motor's previous rating, and the rating reduced by a factor of three, the starting torque would now be about 1,500 per cent of rated.

Although this large starting torque is obtainable, a reasonable value of 200 per cent starting torque can be obtained by increasing the rotor resistance without much sacrifice in rated torque. In the example chosen, a threefold increase in rotor resistance will achieve this result with a net decrease in rated torque of 20 per cent.

A second method of increasing the starting torque is by raising the starting current by decreasing the reactance. The reactance is a function of slot size and proportions, and air gap. Let us assume that the reactance could be cut in half. This would approximately double the starting current, and therefore give four times the starting torque. This method would not affect the losses at full load appreciably. The over-all effect, however, would usually be to decrease the power factor through increased magnetizing current. This method of increasing starting torque has possibilities, but leads to higher starting currents and bad power factor.

Table I. Effect of Rotor Resistance on Motor Rating

		Rated Load		Maximum Torque		Starting Torque	
Frequency	Rotor Resistance	Horsepower	Torque, Pound-Feet	Pound-Feet	Per Cent of Rated	Pound-Feet	Per Cent of Rated
60	1.0	1.0	3.0	9	300	6.0	200
400	1.0	6.7	2.9	16	510	1.8	60
400	20	2.4	1.1	16	1,500	16	1,500
400	2.8	5.2	2.3	16	680	4.8	200

A third way of increasing the starting torque is to alter the motor so that the resistance or reactance, or both, will vary as the motor comes up to speed. In this classification are included double-cage rotors, external rotor resistance operated through centrifugal switches, and many ingenious devices. The small physical size of aircraft motors makes this method quite difficult, but some desirable solutions will be obtained by this method. In fact, all 400-cycle motors take advantage of some variation in rotor resistance from standstill to full load because of the current distribution due to leakage flux in the rotor bars.

A fourth possibility of increasing the starting torque of the motor is by the use of some mechanical device such as a centrifugally operated clutch which would permit the motor to attain speed before picking up the load.

Economy in weight and efficiency of electrical motors can be obtained by designing the correct load so that large starting torques are not required.

Next in importance to the problem of starting torque is the problem of what to do with the maximum torque developed by the induction motor. As noted in the list of requirements, the airplane designer uses such low factors of safety that the maximum torque applied to the load must be definitely limited, sometimes to as little as 20 per cent in excess of the required torque. This same problem gave some difficulty in d-c motors because of voltage and temperature variations. The series motor, however, develops maximum torque at standstill, which is very desirable for most aircraft applications. We have seen that altering the induction motor so that the maximum torque is obtained at standstill results in lower efficiency and consequent increase in pounds per horsepower. In any case, the specified range of voltage and frequency will cause variations in the maximum torque, probably in excess of 20 per cent.

Therefore, it is necessary either to build the structures sufficiently heavy to withstand considerable maximum torque or to use some method of limiting the torque other than motor design. In d-c motors a slip clutch is used successfully since the speed drops off rapidly with increased torque. With an efficient 400-cycle motor the speed at maximum torque is very high, and if a slip clutch is used the motor will heat very rapidly. The thermal protecting device must then operate to open the circuit. Probably a better solution would be the use of a switch operated directly by torque which would open the motor circuit. The use of shear pins in some form is another possible solution.

The problem of the wide range of ambient temperatures does not affect the 400-cycle motor in the same way it affects the 24-volt d-c motor. Low temperature decreases the resistance, which in the case of the d-c motor increases the torque. In an induction motor, the low temperature decreases the starting torque. This difficulty of decreased torque is of academic interest only, however, since the rotor currents will rapidly bring the motor up to operating temperature.

The humidity requirements are easier to meet with the induction motor. In developing motors for use in such equipment as submersible pumps for the Navy, methods of impregnating stators have been brought to a much higher degree of perfection than ever before. We do not anticipate any difficulty due to humidity as it affects insulation.

Altitude will have no effect on the electrical operation of induction motors, except as it influences rating by changing the cooling. Considerable data on this effect has been obtained in the use of d-c motors, and these data are used in the design of 400-cycle motors.

One difficulty which requires close co-operation between the airplane electrical engineer and the motor designer is the question of line drop. We have frequently encountered difficulty in operating high-frequency motors in wind-tunnel testing. For instance one motor developed about 35 horsepower at 6,000 rpm, from 300-cycle service. The short leads used between the control panel and the motor installed in the model doubled the resistance and reactance of the stator. In other words, the line drop equalled the stator drop. This difficulty can be overcome by including the line characteristics in predicting the motor performance. With minimum-size lines, starting torque may be seriously affected if starting current is high.

There is a tendency among some writers of specifications to avoid the use of gear reductions in favor of multipole motors. An eight-pole one-horsepower motor will be twice as heavy as a four-pole one-horsepower motor operating from the same frequency. In addition, increasing the number of poles tends to lower both the efficiency and the power factor. This undesirable effect is greatly pronounced in motors of small size. In addition, the starting torque is relatively lower.

We might summarize this discussion by saying that the 400-cycle squirrel-cage induction motor has great possibilities for increasing the horsepower per pound. Difficulties to be overcome before these possibilities can be completely realized have been discussed. These difficulties are not insoluble. The company with which the writer is associated and many other electrical designing groups have some solutions to all of these problems, and many more solutions will be found.

We have pointed out the necessity of the closest co-operation between the designer of motor and power-supply equipment, the airplane designer, and the control designer. With this co-operation and a clear understanding of the problems involved, the 400-cycle aircraft power-supply system will be a marked success.

Field Determination of Current-Transformer Errors by the Secondary-Voltage Method

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FOR many years there has been a need for a relatively simple field method for testing bushing current transformers for accuracy.

A paper by A. C. Schwager[4] describes a method of predetermining a current transformer's performance based on its admittance-vector locus. A study of this paper suggested the possibility of developing a method of testing such current transformers in the field.

Explanation of Secondary-Voltage Method

In the method to be described in this paper, progressively increasing voltages are applied to the secondary winding of a current transformer with the primary winding open-circuited. In this condition the current transformer builds up an opposing reactive voltage nearly equal to that applied. Thus, only a relatively small amount of current is allowed to flow in the secondary winding. This "exciting" current is made up of two components, that is, a "magnetizing" component and a "core-loss" component. The purpose of this method is to analyze the "exciting" current into its components with the object of combining them by formula with the normal secondary currents and thereby find the phase-angle errors and ratio correction factors for the current transformer under test at certain secondary current values, such as, 0.5, 1, 2, 3, 4, and 5 amperes. Before starting the test the current transformer should be demagnetized, with its primary winding open-circuited, by gradually increasing the secondary-winding test voltage from zero to 50 or 100 volts and back to zero, taking care to protect the test instruments during this operation.

To analyze the exciting current into its two components it is passed through the current coil of a wattmeter, the potential coil of which is energized first by a test voltage (E_v) which is 90 degrees out of phase with the induced secondary reactive voltage (E_s) and then by another test voltage (E_w) which leads E_v by 90 degrees, or is itself in phase with E_s. The vars measured in the first case, with voltage E_v, hereafter shall be referred to as V, and the watts measured with voltage E_w, as W.

The magnetizing component of the exciting current, or that portion which is used to produce voltage E_s, is equal to V/E_v, and the core-loss component is W/E_w.

Susceptance (B), or amperes of magnetizing component per induced volt, is V/E_vE_s, and conductance (G), or amperes of core-loss component per induced volt, is W/E_wE_s.

The higher the resistance (R) and reactance (X) of the secondary burden through which the current transformer has to force the secondary current, the higher will be the value of secondary voltage (E_s) that has to be induced. Consequently, the greater will be the core losses in producing it.

It is convenient to express the errors of current transformers in terms of the phase-angle error (ϕ) and the ratio correction factor (RCF). The phase-angle error may be expressed in terms of its tangent and the four quantities B, G, X, and R.

$$\operatorname{Tan} \phi = \frac{BR - GX}{1 + BX + GR}$$

Ratio correction factor (RCF) is expressed in terms of the same quantities and is equal to $(1+BX+GR)/\cos \phi$. For angles less than $2^1/_2$ degrees the RCF may be expressed as $1+BX+GR$ with an error of less than one tenth of one per cent.

Figure 1 shows the connections required for making the aforementioned tests.

Description of Equipment and Test Procedure

The regulating transformers furnish a means for adjusting the test voltage. If an autotransformer is used for this purpose care must be taken to disconnect completely the secondary winding of the current transformer from ground, as ordinarily the supply voltage already will be grounded.

Potential from the regulating trans-

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Table I

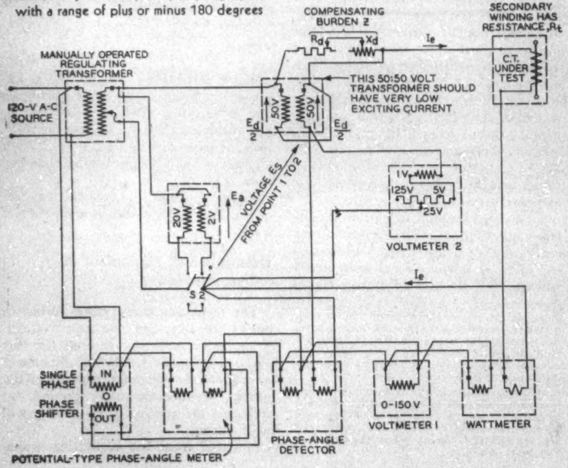
E_s	V	E_v	W	E_w	V/E_v	W/E_w	E_s/N	B	G
0.1	—	—	—	—	—	—	—	—	—
0.2	—	—	—	—	—	—	—	—	—
0.5	—	—	—	—	—	—	—	—	—
1	—	—	—	—	—	—	—	—	—
2	—	—	—	—	—	—	—	—	—
5	—	—	—	—	—	—	—	—	—
10	—	—	—	—	—	—	—	—	—
20	—	—	—	—	—	—	—	—	—
50	—	—	—	—	—	—	—	—	—
100	—	—	—	—	—	—	—	—	—

formers is applied to the current-transformer secondary winding and a compensating burden. For a multi-ratio current transformer a ratio, such as 200–5 amperes, is preferably selected for the tests. The errors for other ratios then may be calculated from curve data obtained at the 200–5-ampere ratio. With switch *S* in the upper position a voltage of 0.1, 0.2, 0.5, 1, and 2 volts is applied by means of a 20–2-volt service transformer. For voltages greater than 2 it may be applied directly with switch *S* in the down position. Then the steps applied for the test are 5, 10, 20, 50, and even as high as 100 or more volts.

Figure 1. Wiring diagram of equipment for testing a current transformer of known turn ratio by the secondary-voltage loading method

The test equipment is as follows:

- Regulating transformer, 120 volts, 60 cycles, manually operated
- Step-down transformer, 60 cycles, 20 to 2 volts
- Insulating tranformer, 60 cycles, 50 to 50 volts with a very low exciting current, for use as an autotransformer
- Compensating burden composed of resistance (R_d) and reactance (X_d)
- Double-throw selector switch (*S*)
- Voltmeter 1, 0–150 volts alternating current
- Voltmeter 2, of high resistance with 0–1–5–25–125-volt a-c scales
- Phase-angle detector with a 150-volt 60-cycle field coil. It is desirable to have the rotor coil, together with its series resistance, wired for the same operating ranges as voltmeter 2 and to be cut into service by the same selector switch
- Wattmeter with scale to suit test conditions, but usually with a 100–200-watt scale
- Potential-type phase-angle meter, 120 volts, 60 cycles
- Phase shifter, 120 volts, 60 cycles, single phase with a range of plus or minus 180 degrees


COMPENSATING BURDEN Z
Rd
Xd
Ie
SECONDARY WINDING HAS RESISTANCE, Rt
C.T. UNDER TEST
MANUALLY OPERATED REGULATING TRANSFORMER
120-V A-C SOURCE
THIS 50:50 VOLT TRANSFORMER SHOULD HAVE VERY LOW EXCITING CURRENT
50V
50V
Ed/2
Ed/2
20V
2V
Ea
VOLTAGE ES FROM POINT 1 TO 2
1V
125V
5V
25V
VOLTMETER 2
Ie
S 2
SINGLE PHASE PHASE SHIFTER
IN
O
OUT
POTENTIAL-TYPE PHASE-ANGLE METER
PHASE-ANGLE DETECTOR
0-150V
VOLTMETER 1
WATTMETER

A compensating burden is introduced into the circuit in order to match the total burden of the current-transformer secondary winding and leads, the wattmeter current coil, and the test leads. The 50–50 volt service transformer is used to subtract this voltage drop, together with the drop across the compensating burden itself, vectorially from the applied voltage (E_a). It thereby secures the true induced secondary voltage (E_s) for use in the phase-angle detector and in indicating voltmeter 2.

Voltmeter 1 is used to determine the values of the voltages (E_v and E_w) that are applied to the wattmeter potential coil when vars (*V*) and watts (*W*) are read on the wattmeter.

The phase-angle detector shown in Figure 1 is an electrodynamometer type of instrument equipped with two voltage coils. It has a fixed coil rated 120 volts and a rotor coil which is designed for variable voltages with full-scale ranges of 1, 5, 25, and 125 volts. The inductance of its coils may be neutralized with suitable shunt capacitors.

A phase-angle meter with two voltage elements also is required for the tests. A synchroscope with a 360-degree scale and a light moving element mounted with its shaft in the vertical position can be calibrated in degrees to serve as a phase-angle meter.

During the test the voltage (E_v) is shifted with the small knob on the phase shifter until it lags E_s by 90 degrees, at which time there will be zero deflection on the phase-angle detector. Vars are read as *V* on the wattmeter, and voltage (E_v) is read and recorded also. Next, the moving element of the phase shifter is rotated counterclockwise until a voltage is obtained that leads E_v by 90 degrees, as indicated by the phase-angle meter. This voltage (E_w) is now known to be in phase with voltage E_s. Maximum deflection of the phase-angle detector occurs at this time. A record is kept of the watts (*W*) indicated by the wattmeter and of the volts (E_w).

It will be found that, as the voltage is increased from very low values, the readings of both *V* and *W* will increase slowly at first and then quite rapidly for the higher-voltage values.

By using a combination wattmeter–varmeter, the 90-degree phase shift is obtained without other adjustments, and the phase-angle meter then may be omitted.

Calculations will be facilitated and repetition of test conditions kept to a minimum if the test quantities are given as in Table I.

It should be noted that $B=V/E_sE_v$ and $G=W/E_sE_w$ where *N* is the actual turn ratio of the current transformer under test.

Preparation of the Two Characteristic Curves

The values calculated in the last two columns of Table I are plotted vertically against the values of E_s/N on the horizontal scale of log–log cross-section paper. There should be three vertical cycles and at least three horizontal cycles on the log–log paper.

Figure 2 shows typical curves of *B* and *G* for a 115-kv bushing current transformer tested at 40 secondary turns and a nominal ratio of 200–5 amperes. From these two curves ratio and phase-angle errors may be calculated for any burden and any ratio within the range of the test data.

The diagonal lines for the various current-transformer ratios are drawn as follows:

1. Draw the diagonal base line for the 200–5-ampere ratio, as shown, between 0.001 on the lower left hand margin and 1.0 on the upper margin.
2. Draw parallel lines starting at distances along the vertical scale equal to the ratio

$$\left(\frac{\text{Actual turn ratio}}{40}\right)^2$$

and along the horizontal scale at distances equal to

$$\left(\frac{40}{\text{actual turn ratio}}\right)^2$$

Application of Characteristic Curves

Since the two characteristic curves are plotted as in Figure 2, the values of B and G and hence the errors may be determined for any current-transformer ratio at any secondary current and burden. For example, at the 100–5-ampere ratio with a secondary burden of $R=0.738$ and $X=0.307$ ohm, with 5 amperes secondary current, the secondary volts (E_s) will be $5\sqrt{0.738^2+0.307^2}$, or 4. For a current-transformer ratio of 100–5 amperes, E_s/N is 4/20 or 0.2. A vertical line is drawn at $E_s/N=0.2$ to intersect the G and B curves as shown. Horizontals from these curves intersect the 100–5-ampere diagonal line at values of 0.091 for G and 0.157 for B. Thus, the phase-angle and ratio errors are found as follows:

$$\text{Tan}\ \phi=\frac{BR-GX}{1+BX+GR}$$

$$=\frac{(0.157)\ (0.738)-(0.091)\ (0.307)}{1+(0.157)\ (0.307)+(0.091)\ (0.738)}$$

$$=\frac{0.1160-0.0279}{1+0.0482+0.0672}=\frac{0.0881}{1.1154}=0.0791$$

$$\phi=+4°\ 31^1/_2{}'\quad \cos\phi=0.9969$$

$$RCF=\frac{1+BX+GR}{\cos\phi}=\frac{1.1154}{0.9969}=1.1185$$

It is usually found convenient to list the quantities as shown in Table II.

Conclusions

From the foregoing discussion it is apparent that a current transformer with primary and secondary turns known may be checked in the field at its secondary terminals in a substation if desired. For bushing current transformers an intermediate ratio, such as 200–5 amperes, is chosen and a range of test voltages applied to the secondary winding varying from 0.1 volt to 100 or more volts. It is not necessary to completely disconnect the current transformer from the high-voltage circuit, provided its primary is open-circuited. In this case one side of the current-transformer secondary winding should be kept grounded.

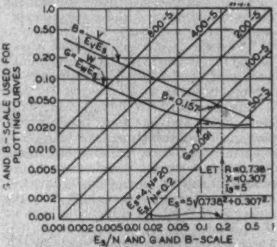


Figure 2. Curves of G and *B* plotted from test data for a multiratio current transformer at 200–5 amperes

Values of G and *B* at any secondary burden and any available ratio are determined as indicated by the arrows

Test equipment may be kept to a minimum, if approximate results are sufficient, by applying the secondary voltage at the current-transformer terminals. The phase shifter, 50–50-volt transformer, compensating burden, and phase-angle detector then may be omitted. The error in this instance will be caused by applying a voltage, E_a, that differs from the induced secondary voltage, E_s, by the voltage drop in the secondary winding and test leads caused by the passage of the test current. If the secondary test voltage is kept below ten volts in testing a bushing-type current transformer of 200–5 amperes, the error in the results from neglecting this difference should be less than two per cent.

From the field data two curves are plotted on log–log cross-section paper with one curve showing the susceptance (B) of the open-circuited current transformer, and the other showing its conductance (G). Values of E_s/N are used for the horizontal scale in each case. Parallel diagonal lines are drawn on the log–log paper to represent all available ratios, and the sheet is then ready for use in determining the ratio and phase-angle errors of any burden at any power factor for any current-transformer ratio, within the limits of the data taken. The RCF in terms of a nominal turn ratio is equal to $\frac{\text{actual turn ratio}}{\text{nominal turn ratio}}$ multiplied by (actual RCF).

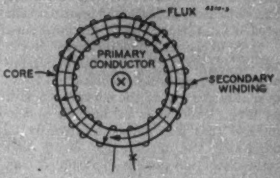


Figure 3. Typical multiratio bushing current transformer

The winding of one ratio only is shown

For metering use the limits 0.001 to 1.0 for E_s/N at the 200–5-ampere ratio covers most of the required metering ranges for all standard bushing-current-transformer ratios. However, for relay use it would be better in all probability to make the tests at 100–5 or at 50–5 am-

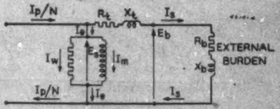


Figure 4. Equivalent circuit of a current transformer in service under load conditions

Summary of the terms used:

R_b = ohms resistance of external secondary burden

X_b = ohms reactance of external secondary burden

R_t = ohms resistance of current-transformer secondary winding. Its effect is the same as if it were connected externally in the circuit as shown

X_t = ohms reactance of current-transformer winding. This is usually so small that it may be neglected

I_w = amperes core loss which $=W/E_w=GE_s$

I_p = amperes primary current

N = turn ratio between secondary and primary turns of current transformer

I_p/N = equivalent primary current (amperes) on a 1-to-1 ratio basis

I_s = amperes flowing in current-transformer secondary windings

I_e = amperes exciting current made up of components I_m and I_w. $I_e=\sqrt{I_w^2+I_m^2}$

I_m = amperes magnetizing current and equals $V/E_s=BE_s$

E_b = volts applied to external burden

Table II

I_s	E_s/N	B	G	BX	BR	GX	GR	BR−GX	BX+GR	Tan ϕ	ϕ	Cos ϕ	RCF
0.5	—	—	—	—	—	—	—	—	—	—	—	—	—
1	—	—	—	—	—	—	—	—	—	—	—	—	—
2	—	—	—	—	—	—	—	—	—	—	—	—	—
3	—	—	—	—	—	—	—	—	—	—	—	—	—
4	—	—	—	—	—	—	—	—	—	—	—	—	—
5	—	—	—	—	—	—	—	—	—	—	—	—	—

peres in order to cover the range for which the data may be desired because of the greater saturation at heavy fault currents.

It is likely that modifications of the curves will yield other data useful in relay and other engineering calculations. However, the purpose of the curves and data as here presented is to describe a field method that may be used primarily to determine the phase-angle errors and ratio correction factors of installed current transformers, of known turn ratio, for metering purposes. Generally this will apply to uncompensated bushing-type and wound-type current transformers, as compensation usually destroys the true turn ratio.

Appendix

Figure 3 shows a schematic drawing of a typical bushing-type current transformer. The multiratio taps are not shown. However, a well-designed multiratio current transformer will be constructed so that, for any ratio, the turns will describe at least one complete helix around the core. For this reason there is very little inducement for the flux to go any place except around the core within the helix. Since the leakage flux is extremely small the reactance component of the short-circuited impedance of the current transformer should be negligible. The actual d-c resistance of the portion of the secondary winding being considered always should be added to the burden resistance to obtain the resistance (R) used in the expressions for tan ϕ and RCF and in evaluating E_s.

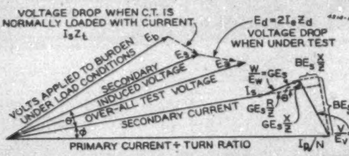


Figure 5. Vectorial representation of voltage and current values within a current transformer carrying a very heavy secondary burden

The diagram shows current and voltage vectors under load and under test conditions

Explanation of the terms used:

R, X, and Z = ohms resistance, reactance, and impedance, respectively, of the current transformer together with its total external secondary burden

$Z=\sqrt{R^2+X^2}$

ϕ = phase-angle error of current transformer in degrees

θ = characteristic phase angle of secondary burden

Tan $\theta=X/R$

E_a = volts applied to secondary circuit under test

Z_d = ohms impedance of secondary compensating burden

E_d = volts drop under test conditions across the total secondary circuit the impedance of which is equal to $2Z_d$

$E_d=2I_eZ_d$

For explanation of other items used in Figure 5 see caption for Figure 4

In Figure 4 an equivalent circuit is shown for a current transformer. On a 1:1 turn-ratio basis, the total primary current (I_p/N) is made up of two parts, I_e, the exciting current, and I_s, the secondary current. The exciting current is dependent upon the amount of secondary voltage per turn that is required. Low secondary voltage per turn means low values of exciting current. High secondary voltage per turn means high exciting current.

The exciting current is made up of two components, I_w, the core-loss component which is in phase with E_s, and I_m, the magnetizing component, which lags E_s by 90 degrees.

The voltage induced per turn in the secondary winding and the total core losses are constant for any constant value of magnetic flux flowing around the core.

Conditions of voltage or coil turns which produce the same core flux will have definite and determinable relations existing between them.

Since the value of flux in the core is proportional to E_s/N (from the formula based on Lenz's Law, $E=4.44NF\phi$ max (10^{-8})) any change in the value of E_s attended by a proportionate change in N for a given current transformer will result in the same value of core flux and in the same total core losses. The core flux is directly proportional to the secondary-induced volts per turn, E_s/N and, for any given value of E_s/N, is also proportional to the magnetizing ampere turns, NI_m or NE_sB. Consequently, E_s/N is also proportional to E_sNB, or B is proportional to $1/N^2$.

The core losses are equal to E_sI_w or $E_s(E_sG)$. They are not necessarily proportional to E_s/N. However, for any given value of E_s/N they are constant and equal to E_s^2G. Hence, if N is made to vary, the corresponding value of E_s for the same core loss as before must vary in direct ratio to N. Therefore, under this condition it may be said that the core-loss E_s^2G, is equal to K, a constant, or that G equals K/E_s^2, where E_s is proportional to N.

Thus, G equals K_1/N^2 for any given values of ϕ, E_s/N, and core loss. K_1 is a proportionality constant.

Furthermore, if the value, G_1 and B_1 are known for a current transformer of turn ratio N_1 at a given value of E_{s1}/N_1, they will be equal to these values multiplied by the ratio $(N_1/N_2)^2$ for any other ratio, such as $^1/_2$, provided E_{s2}/N_2 is equal to E_{s1}/N_1. These are the relations upon which the calculations of Figure 2 are based.

If it is found that the range of operation is too limited with the three-cycle log-log paper the horizontal scale may be extended to four or five cycles.

Figure 5 shows the vector relations with the exciting and core-loss components of current considerably exaggerated. It may be noted that the primary current I_p/N is vectorially equal to the sum of $I_s+GE_s+BE_s$ and algebraically equal to:

$$\frac{I_s+GE_s\left(\frac{R}{Z}\right)+BE_s\left(\frac{X}{Z}\right)}{\cos\phi}$$

where

$$Z=\sqrt{R^2+X^2}$$

The phase-angle error may be found from the tangent of the angle ϕ between I_p/N and I_s, this angle being considered positive when I_s leads I_p/N.

$$\text{Tan }\phi=\frac{BE_s(R/Z)-GE_s(X/Z)}{I_s+GE_s(R/Z)+BE_s(X/Z)}$$

$$=\frac{BRI_s-GXI_s}{(1+BX+GR)I_s}$$

$$=\frac{BR-GX}{1+BX+GR}$$

The ratio correction factor is equal to the ratio I_p/N divided by I_s. Also, $E_s/I_s=Z$. Likewise, from Figure 5 it is apparent that

$$RCF=\frac{I_s+BE_s\frac{X}{Z}+GE_s\frac{R}{Z}}{\cos\phi}$$

or:

$$\frac{I_p/N}{I_s}=\frac{\frac{I_s}{I_s}+\frac{BE_s}{I_s}\frac{X}{Z}+G\frac{E_s}{I_s}\frac{R}{Z}}{\cos\phi}$$

or the

$$RCF=\frac{1+BX+GR}{\cos\phi}$$

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D-C Arc Interruption for Aircraft

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THE present aircraft electric system uses 24 volts direct current; the future system will assuredly be of a higher voltage as aircraft and their generating systems increase in size. Whether it be direct current or alternating current depends on the relative merits of each.[1] If the new system be alternating current the problem of arc interruption is not difficult. If the new system be direct current and above 24 volts, it has been assumed that the arc-interruption problem at altitude would be quite difficult. There is, however, little quantitative data available to bear this out. An investigation was therefore initiated to obtain preliminary data on arc-interruption problems introduced by the use of higher d-c voltages.

The use of a newly developed timer for measuring the duration of arcing made it possible to obtain accurately and rapidly a large amount of quantitative data which are presented herewith.

Scope of Paper

This paper presents data on d-c arc interruption for voltages up to 250 volts at pressures from sea level to 50,000 feet altitude. Both resistive and inductive loads are investigated. The load range chosen includes small currents corresponding to relay or auxiliary-contact values, and higher currents associated with power circuits. Two general types of devices were used for the tests; namely, single- and double-break switches and a blowout-type contactor. The results are presented both in the form of maximum amperes which can be interrupted and arcing time in milliseconds. The effect of inductive loads on arc interruption is discussed briefly.

There also are presented the results of an investigation on the effect of a magnetic field on an arc, which at altitude tends to produce an arc movement in a direction opposite to that at sea level.

No attempt has been made to give theoretical or analytical explanations of the results obtained.

A parallel investigation of the voltage peaks resulting from current interruption was made at the same time and the results of this investigation are presented in a paper by Phillips and Mitchel.[2]

Conclusions

1. The interrupting ability of small gaps (of the order of 0.020 inch) increases markedly with altitude for voltages above a certain minimum (about 80 volts).

2. The interrupting ability of larger gaps (of the order of 0.220 inch) increases with altitude for voltages above a certain minimum (about 175 volts). For these larger gaps the increase is not as marked as for the smaller gap.

3. On a 30-volt system the arcing time *increases* with altitude. On a higher-voltage system (above some minimum depending on the gap) arcing time *decreases* with altitude.

4. For double-break devices handling resistance loads, the maximum volt-amperes which can be interrupted increases rapidly as voltages decrease below 100 volts.

5. The interrupting ability of a double break, each gap being 0.020 inch, on a 30-volt system is about five times that of a single 0.040-inch gap.

6. The arc-interrupting ability of a device improves with increased humidity both at sea level and at reduced pressure corresponding to 60,000 feet.

7. A blowout-type contactor of the type used in industrial work will handle the power requirements of aircraft (100 kva and above at voltages up to 250 volts direct current both at sea level and altitude. Special attention must be paid in the design to ensure successful operation with low currents at altitude.

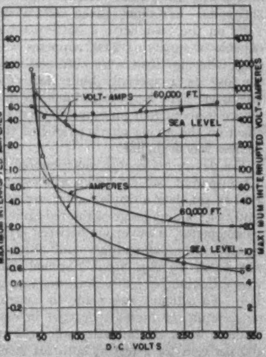

Figure 1. Comparison of the interrupting ability versus voltage at sea level and 60,000 feet altitude for the double-break Switchette on resistive load

Gap: 0.020 inch per break

8. A reverse blowout effect exists at altitude which tends to move a low-current arc under the influence of a magnetic field in a direction opposite to that at sea level. This effect was not observed at currents above 50 amperes. The reverse forces apparently are quite small.

9. There is an effect observable with small gaps on direct current, similar to that on alternating current, whereby it is possible to increase the maximum interrupting capacity of a pair of contacts by *decreasing* the gap.

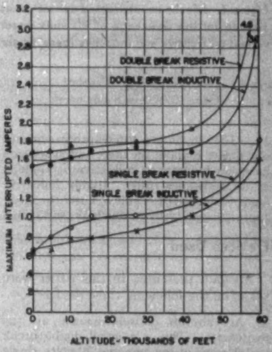

Figure 2. Maximum interrupted current versus altitude for the double-break Switchette at 125 volts direct current on resistive and inductive loads, both single and double break

Load: Inductive *B*, Table I
Gap: 0.020 inch per break

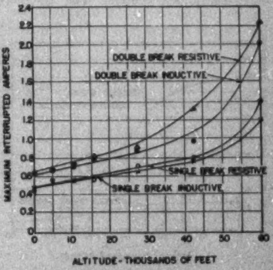

Figure 3. Maximum interrupted current versus altitude for the double-break Switchette at 250 volts direct current on resistive and inductive loads, both single and double break

Load: Inductive *B*, Table I
Gap: 0.020 inch per break

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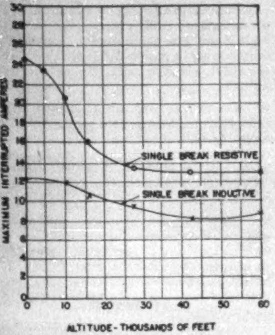

Figure 4. Maximum interrupted current versus altitude for the Switchette at 30 volts direct current on resistive and inductive loads, single break

Load: Inductive *B*, Table I
Gap: 0.020 inch per break

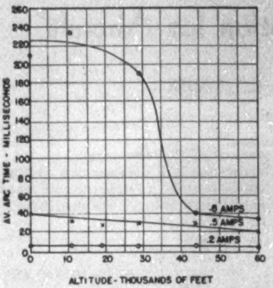

Figure 6a. Average arc time versus altitude for the double-break Switchette at 125 volts direct current on inductive load

Each point represents the average of ten readings of arc time
Load: Inductive *A*, Table I, 0.2 ampere—two coils in series. Parallel combinations of these for higher currents
Gap: 0.020 inch per break

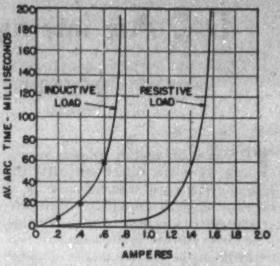

Figure 6b. Average arc time (ten readings) versus current for the double-break Switchette at 125 volts on both resistive and inductive loads at sea level

Load: Inductive *A*, Table I, 0.2 ampere—two coils in series. Parallel combinations of these for higher currents
Gap: 0.020 inch per break

10. The maximum interrupting capacity of a device is about the same for loads which have low inductance (inductive *B*, Table I) as for resistance loads. Highly inductive loads (inductive *A*, Table I) lower the interrupting capacity by as much as two to one.

Devices Used

All data presented in this paper were taken on the following three devices:

1. An aircraft switch known as the Switchette. This is a small double-break switch rated at ten amperes, 30 volts, single circuit, inductive load, at all altitudes. It has silver tips of one-eighth-inch diameter and a gap of 0.020 inch. It requires from one to two milliseconds for the tips to travel across the 0.020-inch gap.

2. A double-break switch with a bridge-type movable contact arm carrying silver tips about one-half inch in diameter on seven-eighth-inch centers. The tip gap was set at 0.220 and 0.312 inch. This unit was part of a commercial size 1 contactor having a tip rating on alternating current of 25 amperes and a motor rating of $7^1/_2$ horsepower at 550 volts. The opening speed is about 20 inches per second.

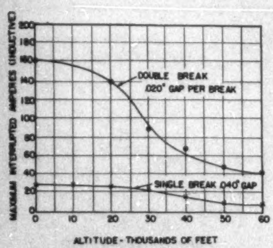

Figure 5. Comparison of maximum interrupted current versus altitude for the 0.040-inch-gap single-break Switchette and the 0.020-inch-per-break double-break Switchette on inductive load at 30 volts direct current

Load: Inductive *B*, Table I

3. An industrial blowout-type contactor with copper tips rated at 100 amperes, 600 volts, direct current. The gap length on this device is $^{19}/_{32}$ inch and there are 11 turns in the blowout field. In this device the arc transfers from the tips to stationary arcing horns so that the opening speed does not usually affect arc interruption.

Method of Test

Power for most tests was supplied by one or two generators rated 200 kw at 300 volts. For some of the tests at 30 volts on the Switchette a battery table capable of supplying 2,000 amperes was used. Arcing times were obtained with the use of a specially built electronic arc timer. The input impedance to this device is about four megohms so that its effect on the arcing characteristic of the devices was negligible.

The device under test was mounted in a transparent bell jar which was evacuated to simulate altitude. Pressure was read on a vacuum gauge calibrated against a manometer. Conversion between pressure in inches of mercury and altitude was based on data from National Advisory Committee for Aeronautics report 218. No attempt was made to control temperature or humidity except in one instance when the humidity was held at 100 per cent for a test at both sea level and altitude. The device was operated at 20 make and breaks per minute for the lower currents (100 amperes and below) and about five to ten operations per minute at the higher currents. In all cases sufficient on-time was allowed for the current to reach its steady state value before breaking the circuit. Ten readings were taken at each set of conditions when obtaining arc-time data so that each point on the presented curves is the average of ten readings. To obtain the data showing the maximum current which a device could handle, the current was increased in small steps taking ten interruptions at each point, until the current was found at which the arc would not extinguish.

Loads—Inductive

The loads used in these tests are those designated in Table I. The inductance

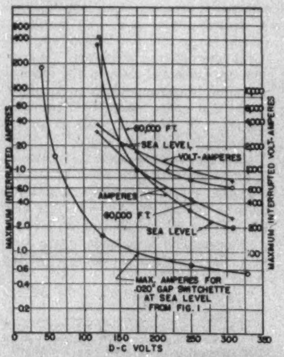

Figure 7. Comparison of the interrupting ability versus voltage at sea level and at 60,000 feet for the 0.220-inch-gap-per-break double-break contactor on resistive load

One curve for the Switchette from Figure 1 is shown for comparative purposes

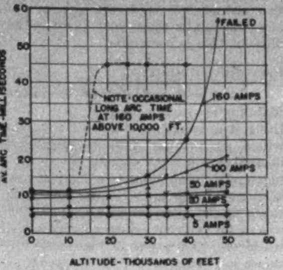

Figure 8a. Average arc time versus altitude for the double-break contactor at 60 volts direct current on inductive load

Each point represents the average of ten readings of arc time
Load: Inductive C, Table I
Gap: 0.220 inch per break

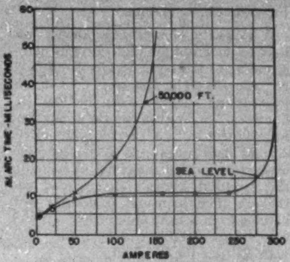

Figure 8b. Comparison of average arc time (ten readings) versus current at sea level and 50,000 feet for the double-break contactor on inductive load at 60 volts direct current

Load: Inductive C, Table I
Gap: 0.220 inch per break

given is that defined as differential inductance.[8] This inductance is obtained from a step-by-step solution of the oscillogram of current rise under constant voltage using the equation $L di/dt + Ri = E$. Although the values of inductance are given only at 50 per cent and 100 per cent current, a continuous curve of inductance versus current can be obtained from the oscillogram.

Discussion of Data

Table II shows the average arc time on a 30-volt system for several commercially available aircraft devices. It is presented here merely to give some factual data and establish the order of magnitude of the arcing times which are now obtained.

SWITCHETTE, 0.020-INCH GAP

Figures 1 through 6 inclusive present results obtained with the Switchette. Figure 1 is a plot of maximum interrupted amperes and maximum volt-amperes versus voltages at sea level and 60,000 feet for resistance load. The rather surprising result should be noted that for voltages above 80 volts interruption *improves* with altitude. The complete curves of maximum amperes versus altitude for 125 volts direct current and 250 volts direct current which were taken for an inductive and a resistive load, both single and double break, are given in Figures 2 and 3. Again it will be noted that interrupting capacity increases with altitude.

Figure 4 compares interruption for resistive and inductive loads versus altitude at 30 volts direct current. Figure 5 shows a comparison of the interrupting ability on inductive load of a double break of 0.020 inch per break with one single break of 0.040-inch gap. The large interrupting ability of the double break at 30 volts is probably due to the fact that the minimum voltage required to maintain an arc in air is approximately 15 volts per break.

Figure 6a shows arcing time versus altitude for three currents at 125 volts. The shorter arcing times at altitude lead to the conclusion that interruption improves with altitude at this voltage, which is in agreement with data shown in Figure 1. Figure 6b shows average arc time versus amperes at 125 volts at sea level for a resistance load and a very highly inductive load. The very sudden change in arc time as the switch approaches its limit is clearly shown. The difference in maximum interrupting ability between this curve and that of Figure 2 is due to the difference in load inductance.

DOUBLE BREAK, 0.220-INCH GAP AND 0.312-INCH GAP

Data were taken on a conventional double-break switch for gaps of 0.220 inch and 0.312 inch. Only the data taken at 125 volts are submitted for both gaps; the majority of the results shown are for the 0.220-inch gap.

The maximum current and maximum volt-amperes interrupted versus voltage for 0.220-inch-gap double-break switch are shown on Figure 7 for a resistance load at sea level and 60,000 feet. Again an improved interruption at the higher voltages is noted, but the voltage above which arc interruption improves is approximately 175 volts for this gap. There is only a slight increase in the current which can be interrupted at altitude compared to sea level for voltages above 175 volts; for voltages below this value the interrupting ability is noticeably less at altitude. A comparison of the maximum currents that can be handled at sea level by the 0.220-inch-gap double-break contactor and the 0.020-inch-gap Switchette is also shown in Figure 7.

The double-break switch, when operated on 30 volts direct current, interrupted currents of 1,000 amperes, both resistive and inductive, without difficulty at both sea level and 50,000 feet. No curve has therefore been plotted for this voltage.

Figures 8a and b show the operation of this device at 60 volts with varying altitude. The point of failure occurred between 100 and 160 amperes at 50,000 feet. It is interesting to note that the arc time

Table I. Load Inductance

Load Designation	Load	Load Data	Load Rating: 100 Per Cent V	Load Rating: 100 Per Cent I	Inductance, Henries a: 50 Per Cent I	Inductance, Henries a: 100 Per Cent I
Inductive *A*	Series and series-parallel combinations of d-c industrial relays; data given for each relay	9,600 turns, 1.5-square-inch iron area, armature sealed	125	0.42	55	8
Inductive *B*	Parallel combinations of d-c industrial relays; data given for each relay	715 turns, 0.6-square-inch iron area, armature sealed	8	2.6	0.10	0.08
Inductive *C*	D-c series-motor field *MDO*-109, 150 horsepower	Series field and commutating field	220	480	0.002	0.0044
Inductive *D*	D-c series-motor field *MDO*-105	Series field	220	120	0.010	0.018
Inductive *E*	D-c series-motor fields *MDO*-105 and 106 in series	Series fields	220		0.012	0.024
Inductive *F*	Aircraft series motor, 1/12 horsepower	Armature and field in series—rotor blocked	24	17.3	0.0033	0.002
Inductive *G*	Aircraft series motor, one-eighth horsepower	Armature and field in series—rotor blocked	24	31.7	0.003	0.003
Inductive *H*	100-ampere-dynamotor contactor coil	2,300 turns, 0.5-square-inch iron area, armature sealed	28	0.54	2.4	1.5
Inductive *J*	Solenoid-type 200-ampere contactor coil		24	0.42	4.2	1.0
Inductive *K*	Balanced-armature-type 100-ampere contactor coil	3,100 turns, 0.09-square-inch iron area, armature sealed	24	0.19		
Inductive *L*	Four-horsepower aircraft-motor field	Shunt field	24	9.6	0.06	0.03

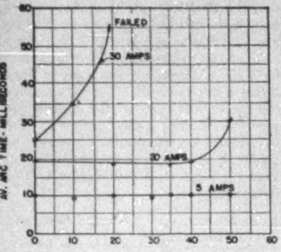

Figure 9a. Average arc time versus altitude for the double-break contactor at 125 volts direct current on inductive load

Each point represents the average of ten readings of arc time

Load: Inductive *C*, Table I

Gap: 0.220 inch per break

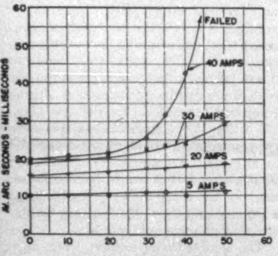

Figure 9b. Average arc time (ten readings) versus altitude for the double-break contactor at 125 volts direct current on inductive load

Load: Inductive *C*, Table I

Gap: 0.312 inch per break

gave an indication of impending failure at 160 amperes by an occasional reading of long arc time before the altitude was reached at which failure finally occurred.

Figure 9a shows the performance of the 0.220-inch-gap switch while Figure 9b shows the performance of the 0.312 inch gap, both on an inductive load at 125 volts. The increased gap has the larger interrupting ability both at sea level and altitude.

Figures 10a and b are taken at the same voltage as Figure 9, the only difference being a higher inductance load. The point of failure has dropped somewhat lower, and the arcing times are longer at the higher amperes.

On 250 volts direct current this device will interrupt only three or four amperes inductive load. It should be noted from curves 7, 8, and 9 that interruption at altitude does not improve for this gap.

Blowout-Type Device

A magnetic-blowout-type device was used to interrupt the high currents corresponding to the power available on modern aircraft. The first tests consisted of interrupting a 40-kva inductive load (inductive *C*) and measuring the arcing time for voltages from 30 to 250 volts direct current at altitudes up to 50,000 feet. The contactor had no difficulty in handling this constant kilovolt-ampere load and the arc time increased uniformly with voltage from 8 to 15 milliseconds. Three was no appreciable change in arc time with altitude for this load.

Figures 11a, 12a, and 13a show the operation of the blowout-type contactor at 60 volts, 125 volts, and 250 volts in arcing time versus altitude. Figures 11b, 12b, and 13b show arcing time at sea level and 50,000 feet versus amperes. It will be noted that a series blowout-type contactor at altitude has an arc-time characteristic very similar to its characteristic at sea level. This means that the device can successfully handle currents from its rating to ten or more times its rating with essentially constant arc time. A blowout-type contactor inherently has a weak point characterized by long arc times at currents around 20 per cent of its rating. This is the range in which the blowout field, because of the low current, is too weak to be effective and the contactor depends primarily on its large gap to break the current. It will be noted that this contactor, although operating successfully at sea level, failed on 250 volts in the low-current range, indicating that altitude has made the interruption more difficult for those currents where the blowout field is ineffective.

Table II. Comparison of Arcing Times at 30 Volts Direct Current

Load: Inductive C, Table I

Device	Gap Break, Inch	Current, Amperes	Average Arc Time (Ten Readings)	
			Milliseconds at Sea Level	Milliseconds at 50,000 Feet
Switchette	0.020	5	5	5
		10	5	5
		50	12	23
Double-break contactor (referred to in paper—devices used)	0.312	5	4	4
		10	4	4
		20	5	5
		50	7.0	7.5
		112	9.5	12.5
		220	7.0	17
Double-break aircraft contactor rated 50 amperes	0.0625	5	5.5	6
		10	6	6
		20	6.5	7
		30	7	7.5
		50	9	10
		100	19.5	24
		150	25	33.5
Double-break aircraft contactor rated 100 amperes	0.125	150	16	22
Double-break aircraft contactor rated 200 amperes	0.125	150	12.5	18.5

Effect of Humidity

One test was made to find the effect of humidity on arc interruption. For this test a container of water was boiled within the bell jar until moisture condensed on the jar. Arcing times were measured on an inductive load at sea level and 60,000 feet. Similar data were taken on the same device with air which was at 20 per cent relative humidity at sea level. The results of these tests are shown in Figure 14. It will be noted that there is some improvement in arc interruption due to increased humidity.

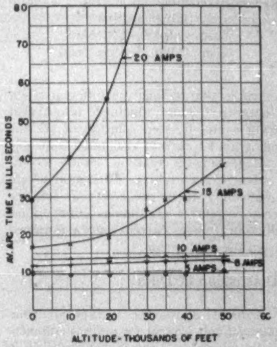

Figure 10a. Average arc time versus altitude for the double-break contactor at 125 volts direct current on inductive load

Each point represents the average of ten readings of arc time

Load: Inductive *E*, Table I

Gap: 0.220 inch per break

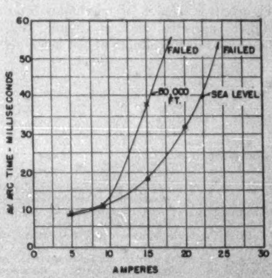

Figure 10b. Comparison of average arc time (ten readings) versus current at sea level and 50,000 feet for the double-break contactor on inductive load at 125 volts direct current

Load: Inductive *C*, Table I

Gap: 0.220 inch per break

REVERSE BLOWOUT AT ALTITUDE

It has been known for some time that at altitude an arc may move under the influence of a magnetic field, in a direction opposite to that in which it moves at sea level. To investigate this effect two devices were used. The first had as electrodes a pair of slightly bowed copper bars mounted to make contact at their center. They were arranged so that the arc could move horizontally, equally well in either direction. A separately excited source supplied blowout-field excitation.

Using only 125-volt d-c supply, arc currents from three to seven amperes, both inductive and resistance loads, and gaps of one-eighth inch, one-fourth inch, three-eighths inch, and one-half inch, it was found that as pressure was reduced the arc moved slower and slower in the normal direction until finally it did not move. The altitude at which this occurred is shown in the following table:

Gap, Inch	Current, Amperes	Direct Voltage	Altitude, Feet
$^{1}/_{8}$	5	125	38,000
$^{1}/_{4}$	5	125	46,000
$^{3}/_{8}$	5	125	47,000
$^{1}/_{2}$	5	125	56,000

For altitudes above those shown the arc was driven in the reverse direction.

It will be noted that as the gap increased the altitude required to stop all movement in the normal direction also increased. Changing the field strength over a wide range did not appear to affect this phenomenon.

The second device used consisted of a double-break switch with two permanent-magnet blowouts and electrodes so arranged that each arc could travel in either of two directions. Using this device with a gap of $^{1}/_{16}$ inch and $^{3}/_{32}$ inch it was observed that the reverse blowout effect did not take place on both gaps, one reversing at altitude and the other merely decreasing in velocity. Investigation revealed that there was a dependence on the direction of current flow. If the cathode was in a strong magnetic field near the magnet the reverse blowout occurred but when the cathode was in a weaker field reversal did not occur.

As the pressure in the vacuum chamber is lowered the arc is observed to move slower and slower. It finally stops moving and then if conditions are right moves in the opposite direction. At this point it is a diffuse discharge, its cross section having increased many times. It is easily observable that the different parts of the arc are acted on by opposing forces, for the arc column will sometimes travel in one direction while the cathode spot moves in another. It appears that the reverse blowout forces act primarily on the region near the cathode spots while the magnetic field as usual acts on the arc column. At very small gaps these same phenomena can sometimes be observed at sea level.

In general, the reverse force appears to be quite small and it may be overcome simply by convection. The reverse arc movement has been reported and discussed in a paper by C. G. Smith.[4]

General Discussion

INDUCTIVE LOADS

There is no standard method of defining the type of load to be used for interruption tests. Often a load is designated merely as "inductive," the inference being that such an inductive load is typical or similar to those which the device must handle. In some cases a time constant for the circuit is specified as in the Navy Aeronautical Specification for contactors *M*569. This is an inadequate designation for it is possible to make up any number of d-c circuits which have the same time constant but whose severity of interruption can be many times different. If an additional specification is made regarding the current at which a certain degree of saturation exists, then a time-constant definition of a circuit is more nearly adequate. Another method of specifying load is to give the dimensions and turns of the test reactor or device. Here again attention must be paid to saturation. The standard test inductors specified by the Bureau of Aeronautics, Navy Department, *E*180, saturate at about 20 per cent of their rated current. This reactor will then have appreciable inductance up to 20 per cent of its rating and negligible inductance thereafter. A more fundamental method of defining the inductance of a circuit for interruption comparisons would be to give the total stored magnetic energy in the circuit. However, this is hard to measure, whereas a curve of inductance versus current can be obtained simply for any combination of devices from the current–time curve.

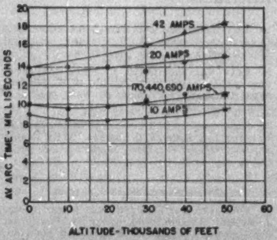

Figure 11a. Average arc time versus altitude for the 0.590-inch-gap blowout-type contactor at 60 volts direct current on inductive load

Each point represents the average of ten readings of arc time

Load: Inductive C, Table I

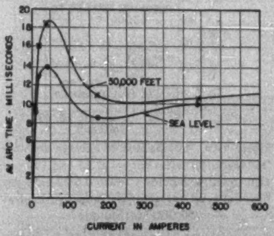

Figure 11b. Comparison of average arc time (ten readings) versus current at sea level and 50,000 feet at 60 volts direct current for the 0.590-inch-gap blowout-type contactor on inductive load

Load: Inductive C, Table I

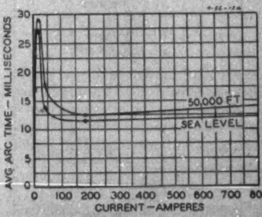

Figure 12a. Average arc time versus altitude for the 0.590-inch-gap blowout-type contactor at 125 volts direct current on inductive load

Each point represents the average of ten readings of arc time

Load: Inductive C, Table I

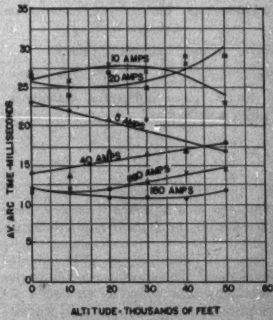

Figure 12b. Comparison of average arc time (ten readings) versus current at sea level and 50,000 feet at 125 volts direct current for the 0.590-inch-gap blowout-type contactor on inductive load

Load: Inductive C, Table I

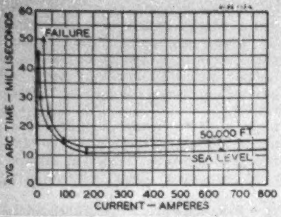

Figure 13a. Average arc time versus altitude for the 0.590-inch-gap blowout-type contactor at 250 volts direct current on inductive load

Each point represents the average of ten readings of arc time

Load: Inductive C, Table I

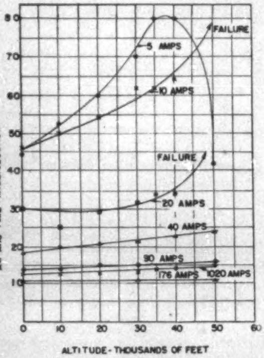

Figure 13b. Comparison of average arc time (ten readings) versus current at sea level and 50,000 feet at 250 volts direct current for the 0.590-inch-gap blowout-type contactor on inductive load

Load: Inductive C, Table I

In this paper differential inductance, which is defined as

$$L=\frac{E-Ri}{\frac{di}{dt}}$$

has been used although it is recognized that it is not the characteristic inductance exhibited by a circuit under arcing conditions. If hysteresis and eddy currents were absent this inductance would be the true inductance to specify for circuit interruption. In any iron-cored circuit, however, especially on direct current where solid iron is used, these core-loss effects are present and so the true inductance which affects arc interruption will vary considerably depending on the current and the speed with which the current is interrupted. The differential inductance is used in this paper because it is readily obtained and is an indication of the inductance of a device.

ARC TIME

It should be noted that the curves present interruption data in two forms—actual arc time in milliseconds and maximum interrupting ability. The latter is often the criterion used to establish the interruption rating of a device, the actual rating being a certain percentage of the maximum current which can be broken. This method of evaluating interrupting ability presupposes a short arc time at rated current and for sea-level tests this is usually correct. However, when investigations of interruptions at various pressures are made, it is advantageous to discover by means of actual arc times the point at which erratic performance begins.

Furthermore, arcing time is a good quantitative measure of the comparative performance of devices. In most cases a sudden increase in arcing time anticipates actual failure of the device. Arc time is also important for evaluating tip life.

For small direct currents where high-voltage surges are usually initiated by the arc interruption, the use of a magnetic oscillograph of the usual sensitivity may lead to erroneous values of arcing time and arcing characteristics.

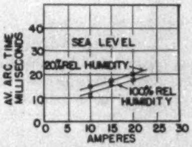

Figure 14a. Comparison of average arc time versus current at 20 and 100 per cent relative humidity with 0.590-inch-gap blowout-type contactor on inductive load at sea level

Each point represents the average of ten readings of arc time

Load: Inductive C, Table I

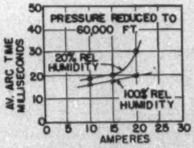

Figure 14b. Comparison of average arc time (ten readings) versus current at 20 and 100 per cent relative humidity with 0.590-inch-gap blowout-type contactor on inductive load at 60,000 feet altitude

Load: Inductive C, Table I

SMALL-GAP EFFECT

The fact that a small gap of the order of one millimeter may have a higher interrupting ability than a larger gap is a well known phenomenon in a-c[5] arc interruption. Investigation of d-c interruption at small gaps has shown that a similar effect exists for direct current. Tests have shown that it is often possible to interrupt more current at either 125 volts or 250 volts, inductive or resistive load, with gaps about 0.005 inch or 0.010 inch than with gaps of 0.030 inch, especially at the higher altitudes. Practical considerations such as metal transfer or the building up of beads on tips due to arcing tend to limit the practical usefulness of this observed phenomenon.

Summary

This paper has presented quantitative information obtained on various direct voltages. It should be cautioned that the maximum interrupted currents given here should not be taken as the rating of a device. The actual rating is a certain fraction of the maximum and is dependent on factors such as factor of safety desired, tip life expectancy, maximum arcing time allowable, and so forth.

Based on data obtained so far, the arc-interruption problem at higher direct voltages appears quite feasible. Possible system voltages are three wire, 250 volt, or even 600 volt as far as arc interruption is concerned.

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Basic Considerations in the Selection of Generators and Batteries for Aircraft

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ELECTRIC systems as used on aircraft have developed from a more or less accessory status in older types of aircraft to a place of prominence in modern types of such importance that safety of operation is often dependent to a limited extent on electric-system reliability. Because of its importance the general requirements of the electric system must be conceived for a proposed new airplane design when the design is in an embryonic state. Preliminary electric-load requirements must be calculated and, as design changes are made, must be revised repeatedly in order to co-ordinate properly the electric-system design with the other design characteristics of the airplane.

During this process of development all controlling design features of the complete electric system should be considered under operating conditions approximating, as closely as possible, those which would be obtained during actual operation.

To facilitate the calculation of electric loads of an airplane in any design stage, and with accuracy consistent with good design, the method described in this paper has evolved from a study of this problem extending over a period of several years.

Method of Analyzing Aircraft Loads

To select a generating source adequate to meet the electric-load requirements of an airplane under applicable operating conditions requires a systematic survey of those conditions with the most severe load combinations that may be encountered. Thus, the number of generators and batteries and their ratings must be determined from the proposed system load calculations with due regard to the variable nature of the loads and to other influencing characteristics.

Since the preliminary studies of electric loads and the determination of required generating capacity for a proposed airplane present the greatest obstacles, the procedure discussed in this paper attempts to clarify this initial phase of design by considering the several factors contributing to the selection of generators and batteries; however, the method used for calculating loads is equally applicable to all stages of design with no variations nor exceptions.

The method of calculation as presented in this paper is founded on the fact that the established standard for rating electric machinery is based on internal heating of the machine; that is, the maximum output of the machine is limited by the maximum safe operating temperature for the type of insulation used. This applies to aircraft generators where the temperature rise of the generator is a function of its internal losses, the most important of which are windage, friction, hysteresis, eddy-current and copper losses. Of these, windage, friction, hysteresis, and eddy-current losses are functions of generator speed and field excitation. Copper losses are the products of the resistances of field and armature and the squares of their respective currents, and usually may be considered the principal source of heating when the generator is operated at 50 per cent rating or above. This is particularly true of present-day main engine generators which are cooled by air blast, since an increase in windage, friction, hysteresis and eddy-current losses due to higher generator speed is compensated by the increased blast resulting from the greater forward airplane speed which usually accompanies an increase in engine speed. Hence, by assuming that all, instead of only the major part, of the losses vary as the square of the armature current, this method of evaluating variable loads is slightly conservative.

The energy which produces heating of the generator conductors in any given period of time, T, is proportional to the integrated squares of instantaneous values of current as shown in the following equation:

$$W = R\int_{T_1}^{T_2} i^2 dt \text{ watt minutes}$$

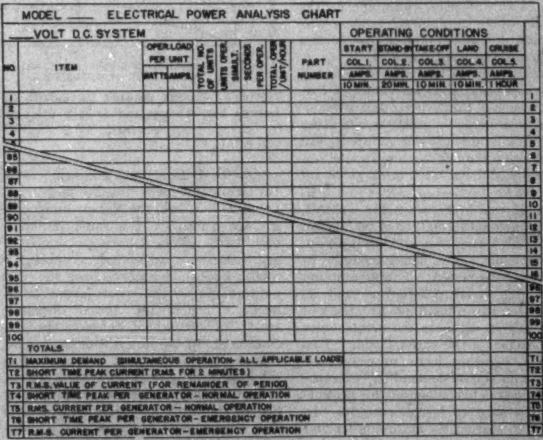
where $T = T_2 - T_1$.

The steady value of current that would produce the same heating is equal to the square root of the mean square of the instantaneous currents integrated over the period T.

$$I_{\text{rms}} = \sqrt{\frac{W}{RT}} = \sqrt{\frac{1}{T}\int_{T_1}^{T_2} i^2 dt}$$

With the aid of this equation the load requirements for each operating condition of the airplane, and consequently the magnitude of the principal factor causing heating of the generators, may be determined, and it is of prime importance that all main operating conditions be analyzed to preclude the omission of any operating characteristics that might influence the generator capacity. The number of different operating conditions for a comprehensive analysis is dependent on the type of aircraft—land based or sea based—

Figure 1. Sample of chart used for tabulating aircraft electric loads



MODEL ___ ELECTRICAL POWER ANALYSIS CHART

___VOLT D.C. SYSTEM									OPERATING CONDITIONS					
NO.	ITEM	OPER. LOAD PER UNIT WATTS	OPER. LOAD PER UNIT AMPS.	TOTAL NO. OF UNITS	UNITS OPER. SIMULT.	SECONDS PER OPER.	TOTAL OPER./UNIT/HOUR	PART NUMBER	START COL. 1. AMPS. 10 MIN.	STAND-BY COL. 2. AMPS. 20 MIN.	TAKE-OFF COL. 3. AMPS. 10 MIN.	LAND COL. 4. AMPS. 10 MIN.	CRUISE COL. 5. AMPS. 1 HOUR	NOTES
1														1
2														2
3														3
4														4
5														5
85														6
86														7
87														8
88														9
89														10
90														11
91														12
92														13
93														14
94														15
95														16
96														96
97														97
98														98
99														99
100														100
	TOTALS.													
T1	MAXIMUM DEMAND (SIMULTANEOUS OPERATION- ALL APPLICABLE LOADS)													T1
T2	SHORT TIME PEAK CURRENT (R.M.S. FOR 2 MINUTES)													T2
T3	R.M.S. VALUE OF CURRENT (FOR REMAINDER OF PERIOD)													T3
T4	SHORT TIME PEAK PER GENERATOR – NORMAL OPERATION													T4
T5	R.M.S. CURRENT PER GENERATOR — NORMAL OPERATION													T5
T6	SHORT TIME PEAK PER GENERATOR – EMERGENCY OPERATION													T6
T7	R.M.S. CURRENT PER GENERATOR – EMERGENCY OPERATION													T7

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and on the use of the aircraft—military or commercial. Operating conditions may include loading, standby, starting, take-off, cruise, combat, landing, and others as may be applicable.

The first step in making the analysis consists of listing each item of utilization equipment on a chart similar to that shown in Figure 1. Adjacent to each item and in columns provided for the purpose are listed amperes per unit, total number of units, units operating simultaneously, seconds per operation, and total operations per unit per hour. Following this, the applicable operating conditions are set up in columns, and the current for each item operating under each of the several conditions is noted.

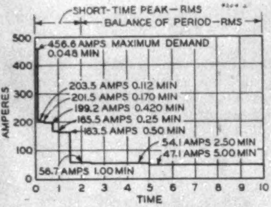

Figure 2. Block diagram illustrating the sequence of loads as used for calculating rms values of current

Individual loads under each operating condition may be classified as continuous loads, which exist for the duration of the operating period considered, and noncontinuous loads, which exist for various portions of the operating period. Examples of these classifications are heating equipment, ventilating equipment, radio receivers, and inverters which are usually operated continuously, while items such as control-surface equipment, landing-gear retraction equipment, and engine starters are operated noncontinuously.

Calculations of maximum demand, short-time peak, and rms values of current for each of the several operating conditions with proper consideration of other influencing factors provide sufficient data to determine the number and capacity of generators and batteries for a given installation.

As indicated in Figure 2 and Table I, it is assumed in this method of calculation that for each operating condition all applicable loads begin simultaneously at the start of the period. This assumption serves to simplify calculations and its justification lies in the impracticability of attempting to estimate the actual order of occurrence of the various loads. This arbitrary sequence leads to pessimistic values of maximum demand and rms current but serves to compensate for neglecting motor-starting inrush currents. In addition to heating, the maximum demand must be considered because of the effect of high momentary currents upon commutation, regulation, and instantaneous torque demand on the generator drive.

Because of the relatively small margins of safety possible in aircraft generator design, it is of great importance that adherence to design ratings be exercised. Many main engine d-c generators have a continuous full-load rating, a two-minute 50 per cent overload rating, and a five-second 100 per cent overload rating, whereas, most auxiliary power-plant generators have a continuous full-load rating and a five-minute 50 per cent overload rating. Thus, it is necessary when analyzing the operating conditions to evaluate the overloads for which there is a possibility of occurrence. This gives rise to the short-time peak current which is the rms value calculated over a two- or five-minute period, whichever is applicable.

The rms value computed for the balance of the period, after the short time peak has been found, is somewhat pessimistic as a basis for determining the continuous-duty capacity of the generator supply, since the arbitrary sequence of the various loads tends toward larger rms values; but, on the other hand, the average value for the same period is optimistic. Thus, if both rms and average values are calculated, a bracket is established which extends from the most unfavorable to the most favorable sequence; hence, the actual operation must fall somewhere between the two extremes. In the absence of large part-time loads the rms and average values will approximate each other very closely, indicating that the sequence of application of the loads for this condition is of little importance; however, if the short-time loads are large their importance becomes of more concern. The difference between the rms and average values over an operating cycle is a function of the ratio of amplitudes of the short-time current and the continuous current, and also of the duration of the short-time current in per cent of a complete operating cycle. Figure 3 presents a family of curves showing the amount that the rms value exceeds the average value when a single short-time load is considered.

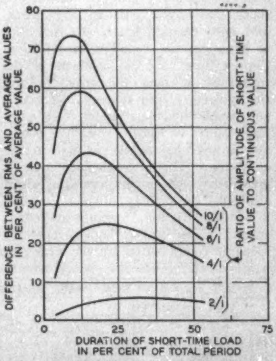

Figure 3. Curves showing percentage difference between rms and average values of current

Table I and Figure 2 serve to illustrate the method of calculating maximum demand, short-time peak and rms currents for a typical operating condition.

Use of Load Calculations in Selection of Generators

With the aid of the most severe service requirements of the power supply, as made available by the calculations of maximum demand, short-time peak, and rms values of current for the several operating conditions, the active generator capacity may be determined. In addition to the active power requirements, other considerations which bear on the selection of the number and rating of generators are: emergency reserve capacity, excess capacity for future system growth, current peaks carried by batteries in low-voltage d-c systems, amount of monitoring that can be tolerated if necessary, altitude rating of generators, generator operating speed, and volt-ampere characteristics of the power-supply system.

EMERGENCY RESERVE CAPACITY

The capacity which should be provided for reserve depends on the likelihood of the generators or their controls becoming damaged or inoperative, and also depends on the importance of reliability of electric power supply, the reliability requirements in general being a function of size and power of the airplane. For almost all types of military aircraft reliability is usually of extreme importance from the

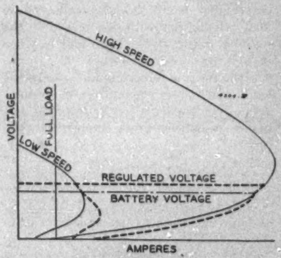

Figure 4. Aircraft-electric-system volt-ampere characteristics

—— Generator volt-ampere characteristic
- - - - System volt-ampere characteristic

standpoints of protection and safety of flight. For large commercial aircraft and especially those for which flight safety is dependent on electrically operated equipment, such as control surfaces, assurance of uninterrupted service is equally important, whereas, for small low-powered aircraft the loss of a generator normally would be of small consequence. Thus, the reserve capacity needed to insure reliability of electric power may vary from 0 to 100 per cent and is purely an arbitrary figure depending on the function of the airplane, flight safety requirements, and the probability of damage to the system.

Reserve capacity is normally provided in the form of more or larger generators, since the weight of batteries to supply power for an extended time is usually prohibitive. However, a complete evaluation of all factors sometimes will dictate the omission of part or all of the normal reserve generator capacity, and in such cases any generator failure will make it imperative to monitor the demand for power by limiting the operation of the utilization equipment. In case all the generator capacity is lost, failure to monitor will result in rapid discharge of the batteries with consequent loss of radio contact, electrically operated engine instruments, and possibly propeller pitch control. In case only part of the generating capacity is lost, the remaining units probably will be operating under an overload. In many ships not having a flight engineer, overload on the generator(s) is very likely to escape attention until the remaining units have overheated and failed. The importance of monitoring before this condition occurs is readily apparent and has led to serious consideration of the desirability of providing a warning whenever serious generator overheating has occurred. The addition of a built-in thermostat in every generator would provide a simple means of operating a red warning light.

Capacity Allowed for System Growth

The amount of capacity provided for future system growth is greatest in the initial stages of the system design, since, as the development of the airplane progresses, the initial allowance probably will be used. It has been common practice to provide from 10 to 25 per cent over and above the initial calculated requirements, depending on the conservatism of the design.

Current Peaks

The maximum demand current for any operating condition is often of a momentary nature which may be within the generator five-second rating previously mentioned. However, in the event that the maximum demand exceeds this rating in either amplitude or time, it is often desirable and advantageous to permit the batteries to supply the additional load on low-voltage systems. This is accomplished by co-ordinating properly the generator-voltage regulator combination to obtain a drooping voltage characteristic, as overload increases, of such nature as to reduce the generator voltage slightly below battery voltage at a predetermined overload. Any momentary current peaks are usually of such short duration that no provisions for added battery capacity are required, although this may not be true if the high-current peaks occur shortly after the batteries have undergone a period of high discharge. Such a condition might occur during take-off after main-engine starting has lowered battery capacity, or during landing if the engines are operated at low speed. Under the latter operating condition batteries would be required to supply a large portion of continuous loads in addition to peaks.

Monitoring

In some instances where maximum demand or short-time peak currents indicate the necessity for exceptionally large generator or battery capacities, it is convenient and practicable to regulate the sequence of operation of manually controlled motors and other loads in order to prevent excessive currents, thus permitting the use of lower rated generators and batteries.

Generator Altitude Ratings

Most main-engine generators maintain full-rated output up to 30,000-feet altitude, above which the output is an inverse function of altitude. The output of auxiliary-power-plant generators, however, is fundamentally dependent on the output of the internal combustion engine which is essentially an inverse function of altitude from sea level. Reduced auxiliary-power-plant ratings above sea level cause them to be very inefficient for altitude operation, and unless engine supercharging is provided, these units are used commonly only for ground supply and emergency reserve in flight, at reduced rating, for main-engine generators.

Generator Speed

To obtain rated output an aircraft generator must be operating within a given speed range with adequate voltage regulation. Thus, the possibility of generator speeds below the lower limit of rated speed must be given proper consideration for those operating conditions during which reduced speeds may occur. A condition of this type may become critical during landing, especially if the approach is made in a prolonged glide from high altitude, but in general would be relatively unimportant during ground operation with the main engines running.

System Volt-Ampere Characteristics

In order to prevent an unstable condition during maximum demand or short-time peak loading from which the system will not recover to normal voltage, the system volt-ampere characteristics must be co-ordinated with operating conditions. The following discussion applies to aircraft power systems consisting of

Table I. Sample of Tabulating and Calculating Loads

Continuous Loads (Current)	Noncontinuous Loads	
	Current	Time
10.4	253.1	0.048
0.3	2.0	0.16
0.2	2.3	0.33
0.4	33.7	0.75
2.5	2.0	1.00
23.0	106.8	1.50
2.7	2.4	2.50
0.1	0.2	2.50
0.5	7.0	5.00
7.0		
47.1	409.5	

I	ΔI	t	It	I^2t
456.6	253.1	0.048	21.92	10,008.7
203.5	2.0	0.112	22.79	4,637.8
201.5	2.3	0.17	34.26	6,903.4
199.2	33.7	0.42	83.66	16,665.1
165.5	2.0	0.25	41.38	6,848.4
163.5	106.8	0.50	81.75	13,366.1
56.7		0.50	28.35	1,607.4
		2.00	2 $\boxed{314.11}$	2 $\boxed{60,036.9}$
			157.0 amperes (average)	30,018.4$^{(1/2)}$ 173 amperes (rms)
56.7	2.6	0.50	28.35	1,607.4
54.1	7.0	2.50	135.25	7,317.0
47.1		5.00	235.50	11,092.0
		8.00	8 $\boxed{399.48}$	8 $\boxed{20,014.4}$
			49.9 amperes (average)	2,501.8$^{(1/2)}$ 50. amperes (rms)

generators with or without batteries which have over-all volt-ampere characteristic curves having a positive slope on the low-voltage end. At the lower end of the generator-design speed range the sytem stability is most critical, since aircraft generators usually are designed, so that the low-speed volt-ampere characteristic curve intersects the regulated voltage curve at approximately 150 per cent of normal rated current. Overload ratings of the generators apply for low-speed operation of this nature insofar as heating is concerned. At this low speed continued increases in load, in excess of the point of intersection of the two curves, result in a relatively sudden drop in the generated voltage; and further increases may reduce the voltage to a point of instability such that the generator excitation fails progressively, causing the generated voltage and load current to drop to small values. Under these conditions the generator is unable to recover voltage until the load has been removed from the system. At the upper end of the speed range the generators are capable of carrying several times normal rated current for periods of time governed almost entirely by temperature rise. However, the intersection of the high-speed volt-ampere characteristic curve and the regulated voltage curve is a limiting loading condition. Loads in excess of this value result in excitation failure and consequent simultaneous reduction of generated voltage and load current. Voltage recovery can be obtained only by removing the load from the system.

The use of batteries in parallel with low-voltage aircraft generators aids in alleviating this unstable condition by increasing the amount of load that may be carried before reaching the unstable portion of the curve. However, the use of enough battery capacity to eliminate completely the positive slope portions of the system characteristic curves is not practicable from a weight standpoint.

The general form of a volt-ampere characteristic curve of an aircraft electric system composed of generators and battery is shown in Figure 4. The effect of batteries in parallel with generators on the system characteristics is shown by the broken line. It will be noted that at low speed the addition of the battery improves the stability of the system appreciably by increasing the load over that which may be carried by generators alone as shown by the solid line.

Each of the previously noted factors must be given due consideration in selecting an economical and efficient generator supply of adequate capacity and reliability.

Selection of Batteries

An aircraft battery is an inefficient source of electric power compared with the generator, but its ability to deliver high short-time currents makes it highly desirable for aircraft application on low-voltage systems. In general, its primary purpose is for ground operations when generator power is not available, while its secondary purpose is for assisting the generators by carrying high peak currents, thereby improving the stability of low-voltage systems. Also, when operated in parallel with generators, the battery acts conveniently as a filter to reduce radio-noise interference.

Two methods for reducing the weights of battery installations have been employed successfully on certain types of airplanes. The first, applicable to land-based aircraft which operate from well-organized airports, utilizes a power supply from an external source connected into the airplane system through a built-in plug-type receptacle. In this type of installation power for engine starting and normal ground loads is supplied by the external source, and the airplane battery is only required to add stability to the system by carrying high-current peaks and to provide emergency power. It is apparent that military aircraft operating from advanced bases which are not equipped with external power facilities are not adaptable to a system of this type. The second method, applicable to medium and large airplanes either land- or sea-based, employs one or more auxiliary-engine generators which provide power for ground operations. In this type of installation the battery need only be large enough to start an auxiliary engine and provide a stabilizing influence on the system.

The following discussion on batteries is based on the assumption that neither of the aforementioned methods for augmenting ground operation power is used.

ENGINE STARTING

Normally, the battery capacity required for a given aircraft installation is basically dependent on engine starting requirements, and as such should provide sufficient ampere-hour capacity for a limited series of starting attempts without completely discharging the battery. In order to assure adequate capacity for this purpose, several factors must be considered: the normal current demands for the size and type of engine starters used, and the effect on starter current and battery capacity of the ambient temperatures that will be encountered. Also, in applications where the battery is normally required to supply power for ground operations in addition to starting, an adequate margin should be allowed, so that engine-starting capacity will not be jeopardized.

Engine starters, although energized for relatively short periods of time, draw currents of exceptionally large magnitude, in some cases amounting to many hundred amperes, imposing severe demands on the battery. Even under the most ideal conditions obtainable, these high-discharge rates reduce the effective ampere-hour capacity of the battery. It should be noted that the discharge period of a battery is not inversely proportional to the current but approaches the inverse square of the current. For example, a fully charged 34-ampere-hour aircraft battery will discharge completely at 6.8 amperes in five hours; at 54 amperes it will take approximately 20 minutes, and at 144 amperes, five minutes, its effective capacity being 34, 18, and 12 ampere-hours, respectively. In addition to the effect of the normally large current demands of the starter on the battery capacity, the effect of low ambient temperature is twofold; first, because the viscosity of lubricants increases the load requirements to overcome friction, and, second, because the effective capacity of the battery drops with the temperature.[2]

ASSISTANCE TO GENERATORS

Improvement of system stability by assistance to generators in supplying high peak currents, is usually assured by the battery capacity determined from other considerations, since the peak demands which exceed generator capacity are usually of very limited duration.

Selection of Auxiliary-Power-Plant Generators

For some applications on large aircraft, where power requirements for ground operations are appreciably more than engine-starting requirements, it is found practicable to utilize auxiliary power plants in addition to main-engine generators. Because auxiliary power plants are normally inefficient at high altitudes, they are seldom used for this purpose except for special applications with supercharging facilities, although their use for medium- and low-altitude operation at reduced rating is not uncommon. The method previously discussed for determining required main-engine generator capacity is directly applicable to auxiliary-power-plant capacity with reservations regarding altitude operation as here noted.

General Applications

The method of computing aircraft electric loads, herein outlined for d-c systems, is equally applicable to a-c systems with proper consideration given to power factor and phase unbalance if on a multiphase system. It is also useful in making preliminary power studies on a wattage basis before system voltage has been established.

Although this method for determining aircraft electric loads may be applied generally to all types of aircraft, the establishment of a definite procedure for selecting the number and ratings of generators and batteries is prohibited by the variable nature of the other influencing factors.

Conclusions

The ever increasing electric applications in aircraft add to the desirability of an adequate flexible method of evaluating loads under practical operating conditions. The method as described in this paper has proved very satisfactory and reliable for this purpose, and when used in conjunction with the other influencing factors it serves well as a basis for selection of generators and batteries.

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2. Aircraft Electrical Engineering (book), Randolph Matson. McGraw-Hill Book Company, Inc., New York, N. Y., 1943, pages 136–9.

One Type of Rotary Magnetic Clutch and Its Associated Brake Used on Aircraft Electric Motors

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EVER since the first application of the electric motor on aircraft, attempts have been made to adapt clutches and braking devices to the motor and its power transmission system with varying degrees of success. Some current objectives are:

1. To limit overtravel of and incorporate accurate positioning in a mechanism, such as landing gears, cowl, or wing flaps of an airplane.
2. To limit the application of load to a motor until such time as the motor has come up to speed.
3. To allow quick reversing of a gear train.
4. To permit temporary disengagement of load at a predetermined torque on the motor shaft (slipping clutch).

With recent accelerated development of aircraft, weight has become a major factor influencing the design of all accessories of such craft. Consequently, the lightweight high-torque high-speed electric motors developed for airplanes have challenged anew the ability of clutch and brake designers.

It is not proposed in this paper to discuss all of the numerous types of nor applications for brakes and clutches, but to consider only one type of clutch and a brake used in conjunction with it. The brake, a design of which is carried out later in this paper, is used to hold a load, through the associated power transmission system after the motor power has been cut off. Another type applies to a mechanism that may be operated manually during an emergency. This necessitates a mechanism which has a free driven disk member.

The clutch considered employs an application of the d-c short-stroke clapper electromagnet. Figure 1A shows the driven disk of the clutch which is made from one solid piece of steel and has the motor-drive pinion cut on its extremity. It is free to move axially through its bearings. Figure 1B is a view of the driving disk. This disk is keyed to the armature shaft (Figure 2) and rotates with it. The stationary clutch coil is wound around a core which forms a part of the inner housing of the driving disk. Figure 2 illustrates a section of the assembled clutch and brake with dimensions indicated. Dotted lines trace the paths of flux. This clutch has been used on a 100-watt 9,000-rpm intermittent-duty motor, developing 0.6- to 1.0-inch-pound torque. It will be noted from reference to Figure 1A or Figure 2 that there is a ring of nonmagnetic material dividing the face of the driving disk into two equal areas. The purpose of this ring is to direct the flux into the driven disk instead of allowing it to shunt across the face of the driving disk. Braking is accomplished mechanically by forcing the brake face of the driven disk (face opposite the clutch engaging one) against a ring of braking material fastened to the housing.

Different materials, including metals, have been used for both braking and slipping clutches. The slipping clutch has met with only fair success on high-speed applications. This, in no small part, is due to a changing coefficient of friction, which is in turn some function of the number of slippages. Steel upon steel has an initial coefficient of friction varying from 0.15 to 0.25. Positive engagement of the clutch allows no slippage; consequently, most clutch coils are in series with the armature. From a standpoint of economy, reliability, and ease of installation, material having a coefficient of friction from 0.2 to 0.4 is the most suitable for braking. Obviously, with the clutch and brake combination just the low-inertia driven disk is braked,

Table I. Absolute Permeabilities for Varying Cross Section

Location	l	A	B	NI/In	μ
l_1	0.0	0.143	84	10.1	8.280
l_2	0.1	0.164	73	8.1	9.060
l_3	0.2	0.185	65	6.5	10.000
l_4	0.3	0.212	57	5.4	10.500
l_5	0.411	0.230	52	5.1	10.200

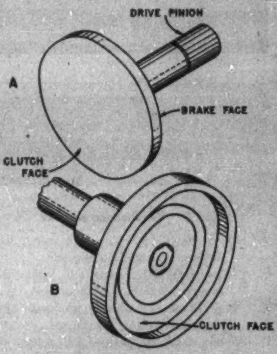

Figure 1. Rotating parts of clutch

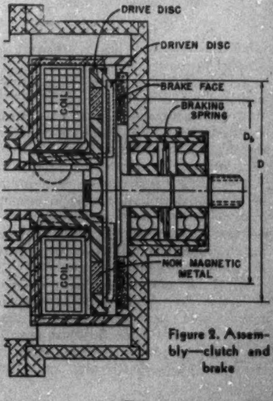

Figure 2. Assembly—clutch and brake

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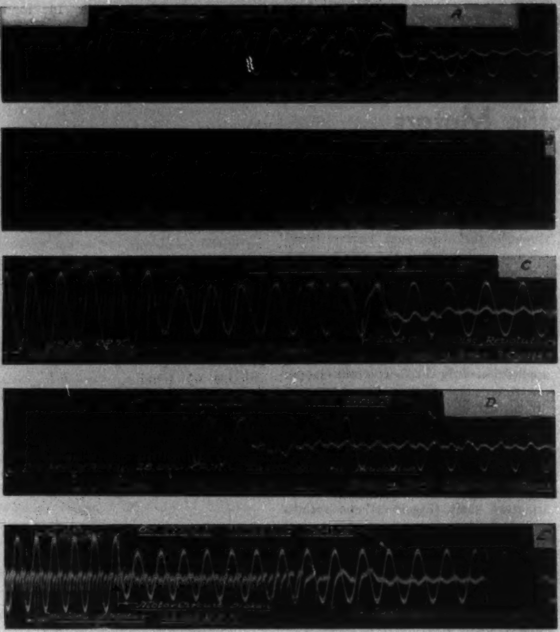

Figure 3. Performance of brake

- A. Motor data—450 watts; 24 volts; 30 amperes; weight, 5.6 pounds
- B. Motor data—100 watts; 24 volts, 8 amperes; weight, 3.25 pounds
- C. Motor data—75 watts; 24 volts; 8 amperes; weight, 1.9 pounds
- D. Motor data—30 watts; 24 volts; 4 amperes; weight, 1 pound
- E. Motor data—30 watts; 24 volts; 4 amperes; weight, 1 pound

while the relatively heavy armature is allowed to coast to a standstill.

Small 24-volt d-c motors with spring-actuated brakes in the output range of 0.02–0.08-inch-pound torque have been reversed as many as 18 times per minute on plugging duty without failure over 60-hour intervals.

The braking performance of a group of high-speed motors ranging in output from 35 to 500 watts is shown in the oscillograms of Figure 3. The accompanying data inscribed upon the prints make the figure self-explanatory.

With the mushroom growth of war activities, this clutch, as is true of so many other new developments, like Topsy, "was not born, but just grew." The following sections entitled "Analysis of the Magnetic Circuit," and "Detailed Design," which have been explained in the foregoing paragraphs, are attempts to pause and consider what is happening in the devices. Concepts and design will be presented straightway with no further explanations and with the hope that they will form a background for further development.

Analysis of the Magnetic Circuit

Figure 4 comprises a part of Figure 2 (assembly) and is used to illustrate this analysis. The path of the magnetic circuit has been divided into five parts, designated as *A*, *B*, *C*, *D*, and *E* for the purpose of calculating a magnetization curve. It was developed by assuming different values of flux, then by calculating the ampere turns necessary to send each value through the various parts of the circuit. This curve is shown in comparison with a test obtained by means of a ballistic galvanometer and exploring coils in Figure 5.

Table II. Ampere Turns for Individual Parts of Clutch

	Lines of Flux				
	4,000	8,000	12,000	16,000	20,000
Part *A*	1.5	2.0	3.0	6	19.5
Part *B*	0.5	1.5	28.0	52	78.0
Part *C*	2.0	3.5	6.0	12	20.0
Part *D*	1.5	2.0	2.8	6	19.5
Part *E*	2.5	3.0	4.0	5	7.0
Total	8.0	12.0	43.8	81	144.0

Parts *C* and *E* have uniform cross-sectional areas. Parts *A* and *D* have varying cross-sectional areas. The procedure used in calculating the ampere turns required for a magnetic path with a varying cross section best can be explained by considering part *D* with an assumed total flux. This flux will be chosen as 12,000 lines, it being approximately the flux required to hold the clutch plates together when the motor is transmitting rated torque. Cross-sectional areas are determined for various locations; the corresponding flux densities are obtained by dividing the total flux by the areas; then from a magnetization curve of the material the ampere turns per inch are read.

The ratio of flux density per square inch to ampere turns per inch is the absolute permeability at the various cross sections. For a total flux of 12,000 maxwells Table I is made, in which NI is ampere turns, μ is absolute permeability, A is area in square inches, B is flux density in kilolines per square inch, and l is distance from the inside of the disk in inches.

If a graph of the absolute permeabilities at various locations is constructed the average absolute permeability can be determined by measuring the area and dividing by the length of the magnetic path. Such a graph is presented in Figure 6. The average permeability is found to be 9.745. Now from another graph of ampere turns per inch versus permeability, the ampere turns per inch are found to be 6.8. This value multiplied by the total length of 0.411 inch gives us a total of 2.8 ampere turns for part *D*. A summation of the calculations is shown in Table II. Adding to the ampere turns

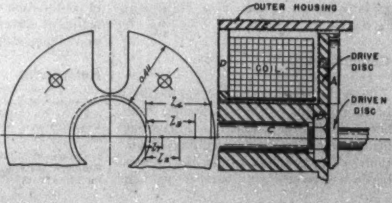

Figure 4. Sections of flux path

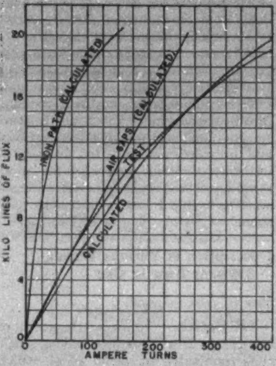

Figure 5. Magnetization curves of clutch

shown in Table II, those necessary to send the assumed values of flux through the air gaps, complete data for the magnetization curve are obtained, as presented in Table III, from which the curves in Figure 5 are drawn. Part *B* has a section in the drive disk which becomes saturated when approximately 8,000 lines of flux are passing through the circuit. Beyond this value some of the flux crosses the air gap into the nut on the shaft.

There are two air gaps to consider in a general analysis. One is the gap between the inner housing and the drive disk or between parts *E* and *A*. The other is between the drive-disk hub and the core or between parts *C* and *D*.

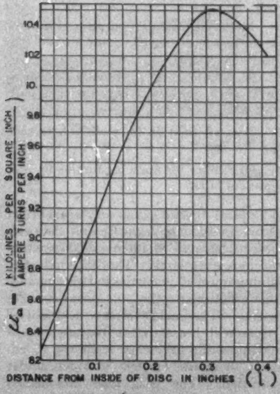

Figure 6. Absolute permeabilities of clutch-coil mounting plate

Table III. Summary of All Data

Lines of Flux	4,000	8,000	12,000	16,000	20,000
Ampere turns, iron path (calculated)	8	12	46	76	144
Ampere turns, air gaps (calculated)	52	104	156	208	260
Ampere turns, total (calculated)	60	116	202	284	404
Ampere turns (test)	50	110	185	285	440

Calculated flux densities expressed in kilolines per square inch in the designated parts with a total flux equal to 12,000 maxwells are as follows:

A	[illegible]	C	D	E
52 to 84	[illegible]	80	52 to 84	44

Detailed Design

The dimensions as indicated in Figure 2 will be used.

Moment of inertia of driven disk (comprised of two sections, the moments of which are computed separately):

$$I=\frac{D^2\pi}{4}\times\text{thickness}$$

$$W=0.28V \text{ (for steel)}$$

$$I={}^1/_2\times{}^1/_{12}\times W/g \text{ pound-inch-seconds}^2$$
$$=M/24$$

Kinetic Energy

$$KE={}^1/_2I\omega^2={}^1/_2I\times\left(\frac{2\pi N}{60}\right)^2=\frac{I\pi^2N^2}{1{,}800}\text{ inch-pounds}$$

Determination of brake spring force to stop disk in *R* revolutions.

A point on the average braking circumference of the driven disk moves RD_b inches.

$$F_1=\frac{KE}{RD_b\pi}\text{ pounds}$$

$$F_2=\frac{F_1}{C_b}\text{ pounds}$$

Disengaging Time

Axial acceleration at disengagement due to $F_2=\frac{F_2}{M}$ inch-seconds.

$$t=\sqrt{\frac{2d}{F_2/M}}=\sqrt{\frac{2dm}{F_2}}$$

Disengaging Slip

$$S=\frac{tN}{60}$$

Determination of flux required to pull driven disk into engagement with driving disk at no load:

$$F_2=\frac{B^2A}{72\times10^6}$$

or

$$B=\sqrt{F_2\times72\times\frac{10^6}{A}}$$

Likewise, the flux required to hold the disks together at any load is calculated as follows:

$$F_3=\frac{T_c}{C_c}$$

$$F_3+F_2=\frac{B^2A}{72\times10^6}$$

$$B=\sqrt{\frac{(F_3+F_2)72\times10^6}{A}}$$

Notation

A, area of clutch face of driving disk, $D^2\pi/4$ square inches
B, flux density, maxwells per square inch
C_c, coefficient of friction, clutch
C_b, coefficient of friction, brake
D, diameter of driven disk, inches
D_b, diameter of braking-ring material, average, inches
d, clutch clearance (between disks) maximum, inches
F, frictional force on brake, pounds
F_2, spring force, pounds
F_3, clutch force required to transmit torque, pounds
g, acceleration due to gravity, 32.2 feet per second per second
I, moment of inertia, pound-inches–seconds²
KE, kinetic energy, foot-pounds
M, mass, pounds
N, motor speed, rpm
S, disengaging slip, revolutions
R, revolutions
t, disengaging time, seconds
T_c, torque to be transmitted by clutch with any applied load, pound-inches
V, volume, cubic inches
ω, angular velocity, radians per second
W, weight, pounds

Conclusion

To fill a long-standing need for clutch and brake development for electric motors, and because this need has been augmented and specialized by our current war-airplane manufacturing program, it seems imperative that the magnetic circuit, flux densities, mechanical and electrical forces, and in addition, the best magnetic coil dimensions be studied in order to perfect the most efficient design of this type mechanism.

It is hoped that this paper will be instrumental in breaking the ground for future development along these lines.

Electric-Wiring Installations in British Aircraft

H. J. HORN
NONMEMBER AIEE

Synopsis: This paper deals with the growth of aircraft electric-wiring installations consequent upon the increasing use of electrical equipment in combat aircraft of all types. A brief description is given of the various proprietary and prefabricated systems of wiring in current use in British aircraft. It concludes with a description of the principles of the standard system now in an advanced state of development which will be applied to all new types of British aircraft now in the design stage.

THE increasing use of electrical equipment in modern aircraft has in its train focused attention on the distribution system. The major items of electrical equipment in British aircraft generally are supplied to the aircraft constructor as government-approved items. It will be obvious that their correct functioning depends upon the wiring installation which serves them. The size, shape, and equipment layout of an airplane does not lend itself readily to an orderly and well-spaced electric installation. The concentration into certain relatively small areas, peculiar in shape, of a large number of electrical items concerned with navigation, control, offense, and defense, is inevitable and constitutes a major problem for the designer. It is perhaps difficult to appreciate that in a modern four-engined bomber, some $4^{1}/_{2}$ miles of single-core electric cable would be required and in a fully equipped two-engined aircraft, $2^{1}/_{2}$ miles would be used. The problem of accommodation and efficient installation of this amount of wiring demands careful thought and planning. It is unfortunate that even up to the present day too little thought has been given to providing an orderly and well-engineered wiring installation and to ensuring well-protected cable routes. The impression still persists in some quarters that electric cables, being flexible, can be moved and disturbed at will to give pride of place to other more clamorous demands. This has resulted in wiring being "slung in" and is responsible for a high percentage of electric failures directly attributable to faulty location of wiring and equipment in situations where ingress of moisture, mechanical damage by air crew, and deterioration by extreme heat all have made their contributions. The Ministry of Aircraft Production, Directorate of Technical Development, now has specified design requirements in this connection which should mitigate, if not entirely overcome, such trouble in future installations.

Elementary Wiring Installations

It was formerly the practice in British aircraft carrying a relatively small amount of electrical equipment, to employ a simple point-to-point system of wiring using single-core cables and the form of terminal block illustrated in Figure 1. This terminal block consists of a bakelite moulding having metal inserts carrying a terminal screw and captive washer. The terminal nests are of the shrouded type, enabling the bared end of the cable to be formed around the terminal screw thus preventing the straying of strands of wire to adjacent points; a simple cover completes the assembly. Such blocks are provided in a useful variety of ways and capacities.

With the growth of the electric installation, this system tends to become cumbrous, involving practically the whole of the preparation and installation work being done on the actual aircraft during construction. With the requirement for rapid production to meet the needs of a growing air force, consideration was given to increased assembly of the unit principle, as employed in the Breeze system, into British aircraft. Subsequently other systems similar in principle were evolved and provided not only for accelerated initial production, but also for more rapid repair and replacement of damaged sections by repair and maintenance personnel.

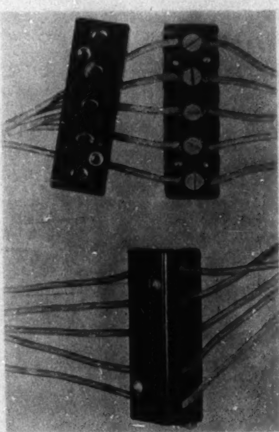

Figure 1. Typical terminal block (old pattern)

Description of Various Wiring Systems Now in Use

THE BREEZE SYSTEM

This system involves the use of branching cable conduits having their ends terminated by metal plugs, which upon assembly into the aircraft are inserted into sockets carried on panels or junction boxes previously installed as completely wired assemblies.

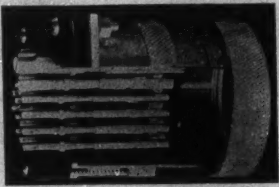

Figure 2. Breeze plug and socket for 7-, 19-, and 37-ampere conductors

The conditions which the designers of this system set out to meet were:

1. Minimum installation time to avoid congestion of personnel on the aircraft during construction.
2. Sectionalization to provide transport joints with quick matching up of aircraft sections.
3. Ease of isolation for maintenance testing.
4. Suitability for mass production by low-standard labor.

Typical Breeze plugs and sockets are shown in Figures 2 and 3, and the principle illustrated is identical throughout the range of sizes. The external shell of aluminum alloy carries bakelite mouldings which house the plug pins or socket inserts. The socket inserts are pressed from sheet brass to the required shape and have the necessary resilience to ensure adequate electric contact under vibration. The plug pins are turned from brass rod or alternatively are "bumped up" from brass wire.

The earlier Breeze installations em-

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ployed flexible metallic conduits but the present practice is to use polyvinyl-chloride conduits except in the few cases where local screening is a requirement. General screening is achieved by suppression at the source of interference.

Figures 4 and 5 show a Breeze-wired main electrical panel for a heavy bomber and a typical Breeze junction box, respectively.

The original Breeze installations employed soldered or screwed-down connections throughout, but more recent installations have incorporated the form of quick release connector block shown on Figure 6. This connector incorporates a solderless (crimped) spade terminal at the cable end.

THE LUCAS LOOM SYSTEM

This system was developed to provide a lightweight system of wiring components and prefabricated looms primarily for installation in fighter aircraft. It differs from the Breeze system in that conduits normally are not used, as the cables are whipped or bound into looms without additional protection. Quick-release connector blocks take the place of plugs and sockets, except in situations where a plug and socket is the more appropriate form of connector to employ. The connector block and other details are shown in Figure 7. The connector block consists of a bakelite moulding designed as a three-way junction. The actual electrical connections take the form of a metal socket split along its longitudinal axis and having pressure maintained by a central spring-loaded screw. Sockets may be either of single-tier or double-tier type. A series of connector blocks is built up as required into a bank by means of a tie rod as illustrated. Backing strips and covers are provided to prevent accidental short-circuiting of connections, and fixing feet are added as necessary.

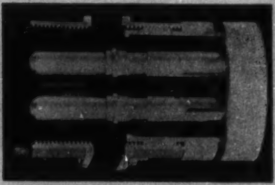

Figure 3. Breeze plug and socket for 64-ampere conductor

The prepared ends of incoming cables are soldered to metal nipples and these nipples act as the plug for insertion into the split sockets.

The procedure adopted for installation of this system into aircraft is as follows. The cable looms for each section of the aircraft (see typical examples in Figures 8 and 9) are suitably whipped and attached to plywood transport boards. These boards are carried into the aircraft, the lashings cut, and the loom run out into position and cleated to prepared positions on the air frame. The nipples are then inserted into the sockets on the connector blocks which previously have been installed in selected positions. Connector blocks and cable ends are identified appropriately to insure correct assembly. Final tightening of the central connector screw on the block ensures a reliable electrical junction.

THE STRIP SYSTEM

The rapid increase in aircraft production imposed an increasing strain on the already overloaded facilities available for the production of the Breeze and Lucas systems. The principal difficulty was in the number of machined parts required and the making up of the conduits or looms. As a means of overcoming these difficulties and to cater to the requirements of new types of aircraft coming into production, the strip system was introduced. The feature of this system was that it relied more on pressed than on turned parts and employed multicore cables instead of conduits or looms. The name "strip system" was derived from the comprehensive use of a ten-way connector of strip form.

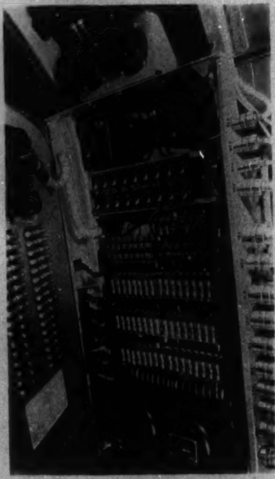

Figure 4. Breeze panel, typical aircraft assembly

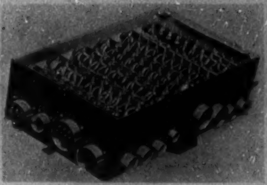

Figure 5. Typical breeze junction box

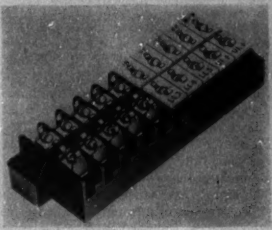

Figure 6. Breeze 20-way terminal block

The ten-way strip connector consists of pressed-copper contacts, silver-plated and gold-flashed, attached securely to a laminated bakelite strip. The contacts are located in slots in the bakelite strip (see item 9 of Figure 10). Between the interior surfaces of the contacts and nested in the central holes in the bakelite strip are stainless-steel springs to provide necessary contact resilience. Two assembly holes—one square and the other round—are provided in the bakelite strip. The prepared ends of the ten-core cable are soldered to the contact-strip extremities.

Five sizes of ten-core cables are provided for main-trunk lines and comprise an intelligent arrangement of 4-, 7-, and 19-ampere cores; rubber dielectric is employed and the exterior sheathing is polyvinyl-chloride.

Item 1 of Figure 10 shows a made-up cable assembly complete with its identification markers and the necessary rubber grommet for attachment to a distribution or junction box.

The distribution and junction boxes vary only in that the former carries fuses whereas the latter is purely a "break" box. Figure 11 shows typical junction boxes for this system. The larger box has a bakelite base, teak sides, metal slides for accommodation of cable grommets, and a metal cover. The small box is an aluminum die casting. Covers of all boxes are provided with double gauze-protected breathing holes.

On installation in the aircraft, the strip connectors with cable attached are

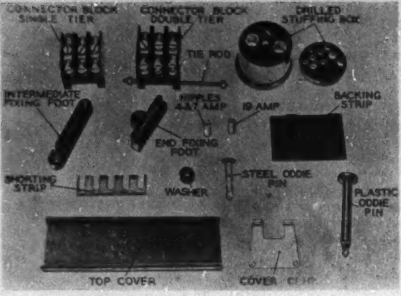
CONNECTOR BLOCK SINGLE TIER
CONNECTOR BLOCK DOUBLE TIER
DRILLED STUFFING BOX
TIE ROD
INTERMEDIATE FIXING FOOT
NIPPLES 4-&-7 AMP
19 AMP
BACKING STRIP
END FIXING FOOT
SHORTING STRIP
WASHER
STEEL ODDIE PIN
PLASTIC ODDIE PIN
TOP COVER
COVER CLIP

Figure 7 (left). Lucas loom-system components

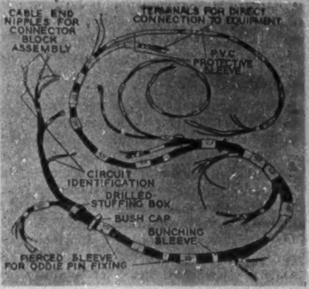
CABLE END NIPPLES FOR CONNECTOR BLOCK ASSEMBLY
TERMINALS FOR DIRECT CONNECTION TO EQUIPMENT
P.V.C. PROTECTIVE SLEEVE
CIRCUIT IDENTIFICATION
DRILLED STUFFING BOX
BUSH CAP
BUNCHING SLEEVE
PIERCED SLEEVE FOR ODDIE PIN FIXING

Figure 8 (right). Typical Lucas loom

assembled on pillars provided in the distribution or junction boxes in accordance with a few elementary rules and instructions which appear on the box cover (see Figure 12). Assembly order therefore is identified fully. The height of the pillars is sufficient to accommodate a maximum number of five strips connector—normally two sets of incoming and outgoing cables and a T-shaped cable. The connector strips have their short ends painted either "red" or "white" and like colors always must be in physical contact and insulated from unlike colors. The action of screwing down ensures efficient electrical connection between connector strips. When less than the maximum number of five strips are assembled, the space remaining is made up by the use of bakelite backing strips (see item 4 of Figure 10). Local single-core cables are attached to the strip connectors in similar manner to the main ten-core cables as shown in item 10 of Figure 10.

The principle of application of the strip system is similar to that employed for the Breeze and Lucas systems. Looms of multicore and local cables complete with their strip connectors all appropriately identified are brought into the aircraft, and cleated into position on the airframe, and finally the strip connectors are assembled in the distribution or junction boxes previously installed.

Standard spare lengths of ten-core cable assemblies are available for replacement and repair purposes.

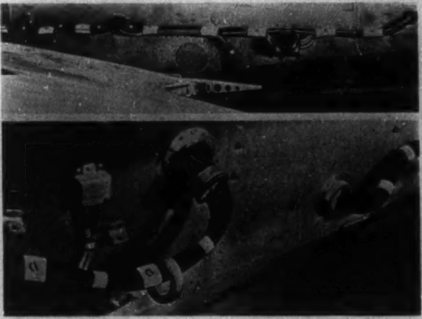

Figure 9. Typical Lucas loom installed on aircraft

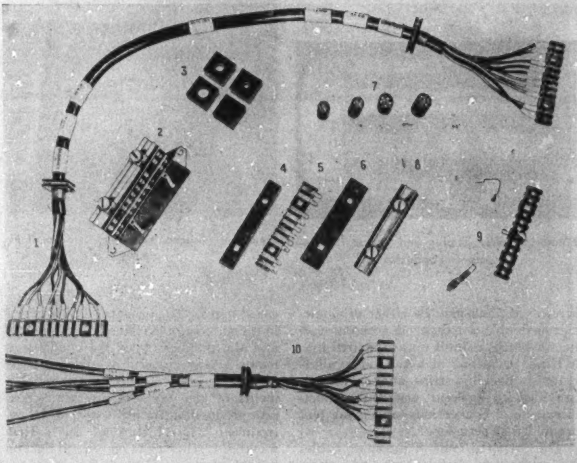
1
2
3
4
5
6
7
8
9
10

Figure 10 (below). Strip-system components

Development of the Standard System

The wiring systems just described have given reasonably good service under wartime conditions, but the existence of at least three distinct and varying systems of wiring presented difficulties of maintenance and storekeeping which can well be imagined. Consideration therefore has been given to the development of a standard system of wiring which could be applied comprehensively to all new types of British aircraft. The existing systems were examined critically to ascertain whether any of them could be adopted as the approved standard, but it was found that each exhibited weaknesses of greater or lesser degree. The Royal Air Force and Fleet Air Arm representatives were consulted on their experience of actual field conditions and expressed a strong preference for stand

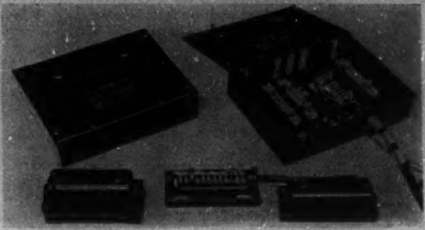

Figure 11. Typical strip-system junction boxes

ardization of a system akin to the Lucas lopm system but with improvements in insulation value, resistance to corrosion, and maintenance facilities. This recommendation formed the basis on which development proceeded under the guidance of a committee representative of the Society of British Aircraft Constructors, the electrical industry, and the Directorate of Technical Development of the Ministry of Aircraft Production. The development of the standard system and components is not yet completed in every detail, but the following is a description of the main principles and component parts:

1. The main points which needed to be borne in mind during this development were: simplicity of engineering, ease of production, flexibility to accommodate modifications, economy of weight and space, efficiency under all conditions of operations. The system is based on the use of single-core cables for general wiring, the reduction to the minimum of junctions and joints, and the use, as generally as possible where breaks are essential, of efficient connector blocks.

2. Connector blocks for cables up to and including 37-amperes capacity are of the "quick-release" form. A typical 19-ampere block is shown at Figure 13. It will be noted that this block conforms closely to the principle of the Lucas block and comprises a bakelite moulding dimensionally designed to give the maximum creepage path consistent with reasonable size and weight. Apertures right through the block ensure free circulation of air, drainage of deposited moisture, and rapid drying. Molded-in inserts are provided for the reception of socket assemblies, either single- or double-tier as illustrated. The sockets are split along the longitudinal axis and pressure is controlled by a spring-loaded screw. All metal parts employed are noncorrodible.

3. The 19-ampere blocks will be available in 2-, 3-, 5-, and 15-ways and covers are provided for use when required. Identical in principle, the 37-ampere blocks are slightly larger and are available in two- and three-ways.

4. Cable ferrules similar to the Lucas nipples are attached to the cable ends by being crimped to both the cable core and the dielectric, providing thereby a rigid bond and support for the joint.

5. For cables of 64-ampere capacity and larger, a heavy-duty connector block will be provided. This consists of a bakelite moulding having a molded-in central pillar and a recess for the reception of the cables. Pressed cable lugs of the palm type are employed. These are assembled as required on the central pillar and securely clamped by a lock nut. There are no multiple combinations of this block. Attachment of lugs to the cable core normally is done by clamping, but soldering is also demanded and preferred.

6. Suitable hand crimping tools have been designed for attaching ferrules and lugs, but it obviously will be necessary to consider bench and power-operated tools when full production of the system arises.

7. Junction boxes will not be used in connection with the standard system in situations where the aircraft structure or a cover of simpler form than a box (for example, a canvas cover) provides sufficient protection for banks of connector blocks. For exposed positions, however, or in situations where mechanical damage is possible, standard boxes will be provided which are designed to be splashproof and capable of draining away any accumulation of moisture due to internal sweating.

8. Fuses generally will be mounted on panels and designs are in hand for combined fuse holder and connector blocks to cover the standard fuse range up to and including 100 amperes. Above that capacity it is probable that circuit breakers of the trip-free type will be used. A standard identification scheme also has been produced concurrently with the foregoing development and will become an integral part of the standard system.

9. Cable looms will be prepared external to the aircraft and installed in similar manner to that outlined for the Lucas system. The electrical installations for new types of aircraft now being designed will employ the standard system.

Cables

Hitherto on British aircraft, there has been a general use of "cel"-type cables. These cables employ cellulose-lacquered cotton cambric as the external sheath and rubber as the dielectric. In the past "cel" cables have given reasonably good service, but there has been difficulty under wartime production conditions in ensuring the necessary high-grade finish of the sheath to resist attack by gasoline, oil, and hydraulic fluid. There is now a tendency to employ polyvinylchloride as the external sheath in view of its high resistance to action by these fluids.

A further development now in the initial stages has been occasioned by the gradually deteriorating quality of the rubber permitted for use as a dielectric. From this development sample cables employing polythene as the dielectric have been produced and appear to be

Figure 12 (left). Typical strip-system aircraft assembly

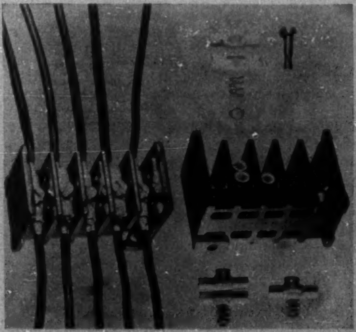

Figure 13 (right). "Standard" system five-way: 19-ampere connector block very promising. An additional advantage accruing from the use of polythene in place of rubber is the possibility of eliminating the tinning of the copper cores.

Conclusion

From the foregoing it will be seen that developments are proceeding actively in Britain to provide electric-wiring installations for aircraft which will be reasonably efficient in operation and capable of being produced by wartime labor and production methods. The experience of four years' war operations in a variety of locations has been studied and employed as the basis for this development. Among these experiences is that gained by British personnel in operating and maintaining American types of aircraft in which single-pole electric installations are employed. Although until recently all British aircraft employed an insulated two-pole system, new types of aircraft are being engineered on the single-pole system employing the air frame as the negative return. The adoption of this practice will be advantageous from the points of view of weight saving and the ability to use electrical equipment which has been designed for this system of supply.

Airline Aircraft Electric-Equipment Maintenance

W. A. PETRASEK
ASSOCIATE AIEE

"ANNOUNCING the departure of trip 23, Nashville–Memphis–Phoenix–Los Angeles, leaving at gate 6, all aboard, please." In a matter of minutes after this announcement an airliner departs on schedule to carry some 5,000 pounds of passengers and cargo over rivers, mountains, and cities and to deposit them safely at the destination. Another routine flight has been made possible by the combined efforts of a small army of airline employees.

Electrical engineers will be interested to know that a large part of the success of the trip is the result of their efforts in the development of reliable aircraft electric equipment. So dependent is the present-day air transport upon its electrical equipment that without it airline operation as it is known today would be quite impossible.

The foregoing statement is not to be misconstrued to mean that an aircraft's electrical system is essential for successful flight, since in aeronautical terminology, a distinction is made between the engine electric-accessory equipment necessary for engine operation, namely, magnetos, booster coils, ignition wiring, and so forth, and the remainder of the electric system such as the generators, instruments, lights, radio, and similar equipment. Although it is entirely possible to take off and fly an airplane without the aid of any of the latter equipment, long-distance flights or even flights extending beyond the visibility of the airport would be extremely hazardous except under the very best conditions of daylight flight, with ceiling and visibility unlimited weather, and freedom from icing conditions.

It is the purpose of this paper to present the airline operator's problem of insuring reliable operation of the aircraft electric equipment. This problem will be presented from the standpoint of operation of American Airlines' fleet of 50 Douglas *DC*-3 transport airplanes, the conventional 21-passenger low-wing all-metal monoplane. The following statistics will give some idea of the magnitude of the problem. During the year of 1943 to 1944, some 449,556,248 passenger miles were flown with an average flying time of 10.2 hours per day per airplane. This flying was done over 8,369 miles of routes extending from New York to California and from Canada to Mexico and serving some 45 cities in that area.

A glance at the organization chart of American Airlines' maintenance department (Figure 1) will show the arrangement which has been found necessary to accomplish the job of maintaining a fleet of airplanes. It is impossible to discuss the problem of aircraft electric-equipment maintenance without considering the entire system, inasmuch as the basic maintenance procedures are formulated from the operating experience with all components of the airplane and are carefully planned to produce the maximum effect with the minimum cost. The following is a thumbnail sketch of the maintenance system and procedures.

Briefly, there are four stations designated as base stations, one each at Burbank, Calif.; Fort Worth, Tex.; Chicago, Ill.; and La Guardia Field, New York, N. Y. In addition all the overhaul facilities: shops, engineering, and administrative departments are located at La Guardia Field. All other stations in the system are commonly referred to as line stations.

The chief responsibility of the maintenance department is to keep the airplanes airworthy. The Civil Aeronautics Administration has outlined in some detail in the regulations the definition of airworthiness as it applies to all parts of the airplane. A unit of equipment designated as airworthy obviously must have a certain degree of reliability. The reliability of an article can be controlled to some extent by the application of inspection and overhaul procedures; however, the application of such procedures increases the cost of operation and some economic and workable balance must be obtained.

The airline's yardstick of service is the hour. One hour of service on a part means that it has flown for that period of time clocked from wheels off to wheels down and represents approximately 155

Table I. Electric-Equipment Overhaul Time Table

Equipment	Overhaul Period	
	Normal	Maximum
Generator control box	400	425
Landing-light bulbs	400	460
Starter motor	Every engine change*	
Propeller-deicer pump motors	Every engine change	
Generators	Every engine change	
Tachometer indicators	Every engine change	
Carburetor-temperature bulb	Every engine change	
Oil-temperature bulb	Every engine change	
Thermocouple washer	Every engine change	
Exhaust-analysis cell	Second engine change	
Tachometer magneto	Second engine change	
Voltammeter	Second engine change	
Pitot-heater ammeter	Third engine change	
Fuel-quantity indicator	Third engine change	
Exhaust-gas-analyzer indicator	Fourth engine change	
Air-temperature gauges	Sixth engine change	
Carburetor-temperature gauges	Sixth engine change	
Cylinder-head-temperature gauges	Sixth engine change	
Fuel-quantity tank units	Sixth engine change	
Oil-temperature gauges	Sixth engine change	
Synchroscope	Sixth engine change	
Starter relays	1,500	2,000
Argon-light dynamotor	1,500	2,000
Wing-deicer valve motors	1,500	2,000
Flasher mechanism, navigation lights	3,500	3,750
Pitot heater	4,500	5,000
Master switches, battery	5,000	6,000
Primer solenoids, booster coils	5,000	6,000
Fuel-flowmeter power supply	5,000	6,000
Landing-light relays	6,500	8,000
Electric system, including conduit, wiring, junction boxes, lights, switches, and so forth	(Airplane overhaul) 6,500	8,000
Batteries	Life of battery	

* Engine-change period—normal—750 hours; maximum—775 hours.

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miles of flight. Every airplane is subjected to a total of four types of inspection and one period of overhaul. The most frequent inspection is the service inspection which is conducted at every regular schedule stop and which consists of a visual check of the airplane's control surfaces and propeller blades, and a check of the oil and gasoline in the tanks. At that time any pilots' complaints are acted upon where necessary to render the aircraft airworthy. This inspection will occur on the average of every two hours of flying time and approximately four times a day for each airplane.

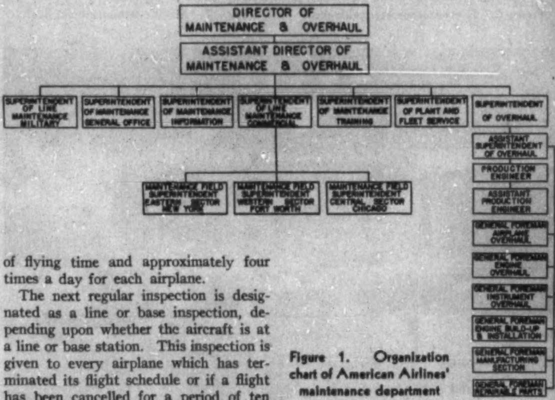

DIRECTOR OF MAINTENANCE & OVERHAUL
ASSISTANT DIRECTOR OF MAINTENANCE & OVERHAUL
SUPERINTENDENT OF LINE MAINTENANCE MILITARY
SUPERINTENDENT OF MAINTENANCE GENERAL OFFICE
SUPERINTENDENT OF MAINTENANCE INFORMATION
SUPERINTENDENT OF LINE MAINTENANCE COMMERCIAL
SUPERINTENDENT OF MAINTENANCE TRAINING
SUPERINTENDENT OF PLANT AND FLEET SERVICE
SUPERINTENDENT OF OVERHAUL
MAINTENANCE FIELD SUPERINTENDENT EASTERN SECTOR NEW YORK
MAINTENANCE FIELD SUPERINTENDENT WESTERN SECTOR FORT WORTH
MAINTENANCE FIELD SUPERINTENDENT CENTRAL SECTOR CHICAGO
ASSISTANT SUPERINTENDENT OF OVERHAUL
PRODUCTION ENGINEER
ASSISTANT PRODUCTION ENGINEER
GENERAL FOREMAN AIRPLANE OVERHAUL
GENERAL FOREMAN ENGINE OVERHAUL
GENERAL FOREMAN INSTRUMENT OVERHAUL
GENERAL FOREMAN ENGINE BUILD-UP & INSTALLATION
GENERAL FOREMAN MANUFACTURING SECTION
GENERAL FOREMAN REPAIRABLE PARTS

Figure 1. Organization chart of American Airlines' maintenance department

The next regular inspection is designated as a line or base inspection, depending upon whether the aircraft is at a line or base station. This inspection is given to every airplane which has terminated its flight schedule or if a flight has been cancelled for a period of ten hours or more. Two men spend approximately five hours in making a comprehensive visual inspection of the aircraft's exterior and of its interior accessory equipment. At this time a functional check of the electric system is made, and the airplane's storage batteries are replaced with freshly charged batteries. All units are checked for their replacement time and are changed if necessary. Each aircraft receives a line or a base inspection at least once every 24 hours (Figure 2). A base inspection is essentially a duplication of the line inspection with the addition of a few more complete functional checks made possible by the use of test equipment located at the base stations. Approximately 75 items of equipment are reviewed at the time of a base inspection.

A major inspection is given each aircraft when 200 hours of flight time have been accumulated with a maximum limit of 230 hours since the last major inspection. A major inspection is performed at a base station and requires approximately 100 man-hours. It is a repetition of the aforementioned base inspection with the addition of a functional check of the hydraulic and landing-gear system with the ship on jacks. A more comprehensive engine inspection is made and consists of an oil change and a check of the valve clearance and of cylinder compression. The calibration of the exhaust-gas analyzer (fuel-air-ratio indicator) is checked by passing a test gas of known ratio through the analysis cell (Figure 3). The major inspection covers some 100 items of the aircraft.

In addition to the various airplane inspection periods the maintenance procedure requires that every component of equipment be overhauled periodically. The time between overhaul for any accessory is determined primarily by the past service experience with that equipment. Furthermore, wherever possible this time is adjusted to coincide with one of the inspection or engine-change periods. As it turns out most of the periodic replacement times are equal to or multiples of the engine-change period. This period at the present time is 750 to 775 hours. Those electric accessories which form a part of the engine-mount assembly are expected to give reliable service for at least the engine-change period, thereby facilitating the handling of such parts during their replacement. These units include the aircraft engine-driven generators, starter motors, magnetos, booster coils, resistance-type temperature bulbs, and tachometer generators. The engine spark plugs are the outstanding exception to this program, requiring replacement every 60 hours.

Other electric equipment not necessarily a part of the engine assembly also is removed during engine-change periods and at various multiples thereof. Table I indicates the various times at which the different items of electric equipment are removed for overhaul.

Figure 2. Battery service at line inspection

Figure 3. Exhaust-gas-analyzer-cell installation

It is interesting to note the increase in engine-change periods during the past six years as this is a factor which directly affects the periodic replacement time for the majority of electric equipment (Figure 4). That is, if the overhaul time of various accessories could not be advanced with the engine-change time, the entire maintenance program would be seriously inconvenienced. There has been some discussion in regard to raising the engine-change time to 1,000 hours, and it is not unreasonable to expect such a change in the next few years. In view

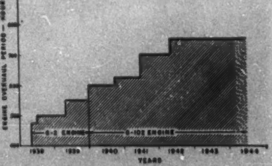


Figure 4. Trend of engine-overhaul period

Wright G-2 and G-102 engines

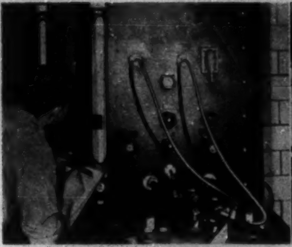


Figure 5. Fuel-flowmeter calibration

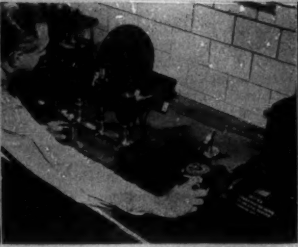
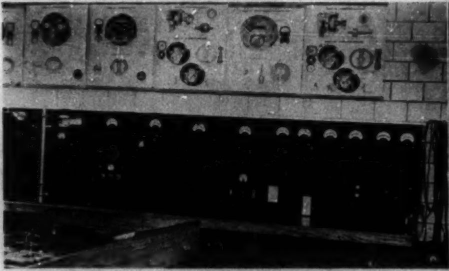


Figure 6. Engine-tachometer calibration

Figure 7. Electric-instrument overhaul



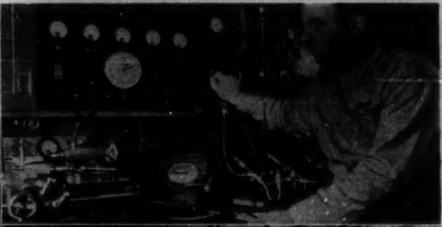
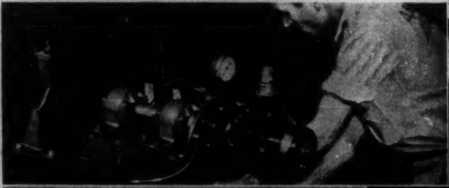
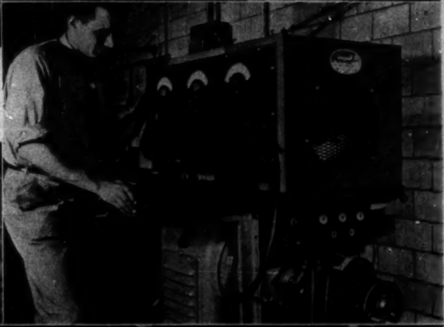


Figure 8. Electric-instrument calibration bench

Figure 9 (right). Bench test of electric-motor-driven accessories

Figure 10 (lower right). Generator and control-box test stand





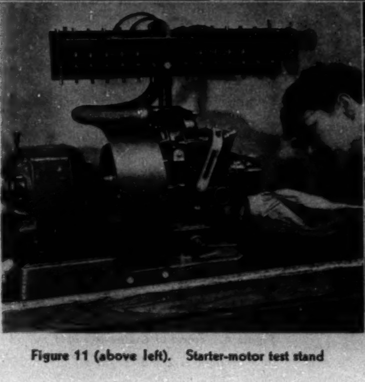


Figure 11 (above left). Starter-motor test stand

Figure 12 (left). Magneto test stand

Figure 13. Spark-plug overhaul

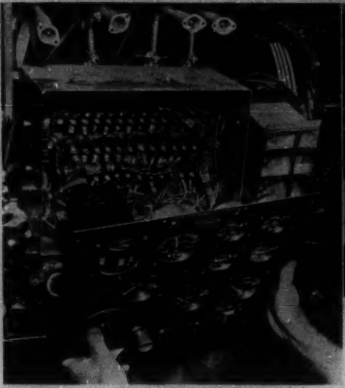


Figure 14. Electric-instrument-panel removal at overhaul

Figure 15 (top). Center section, electrical junction box

Figure 16. Pilot's electric switch panel

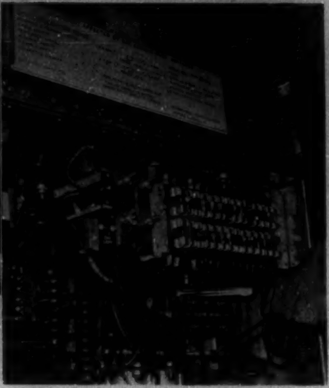


Figure 17. Main electric junction box

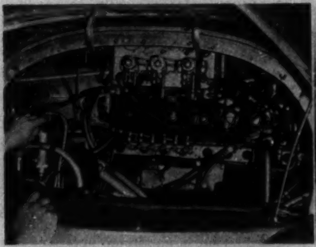


Figure 18. Rear view of instrument panel of Douglas *DC*-3 airplane

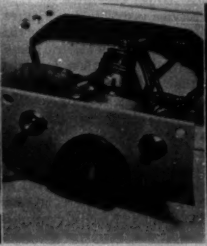


Figure 19. Individual-seat-light installation

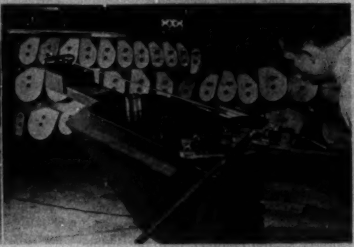


Figure 20. Fabricating electric conduit for replacement

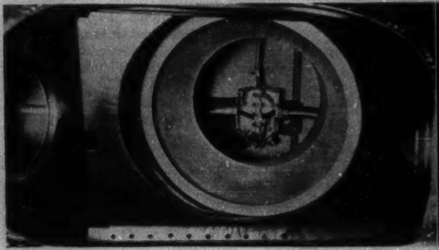


Figure 21. Landing-light installation of the leading edge of the wing

Figure 22 (above right). Engine electric-harness overhaul

Figure 23 (right). Engine ignition-harness overhaul

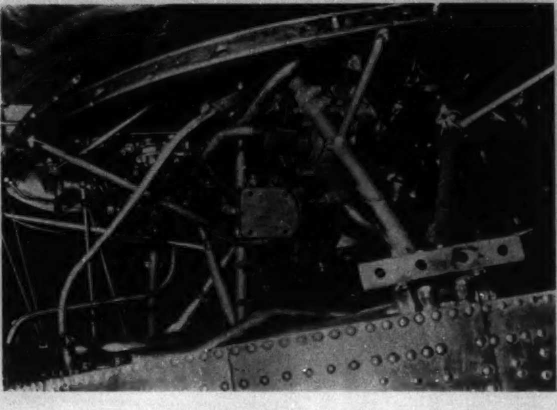


Figure 24. Space available for servicing engine electric accessories

of this trend in engine-change period, it is somewhat disappointing to note the newly created Army–Navy and other specifications setting the continuously operating time requirement for accessory equipment at 400 hours. It sincerely is hoped that the aircraft electric-equipment manufacturers will design for the 1,000-hour mark so that their equipment will fit in with the expanding airline requirements. In this respect the manufacturer should consider that the airline operator must balance maintenance cost against the operating cost and that, in some instances, the heavier but less troublesome article is the less expensive in the long run.

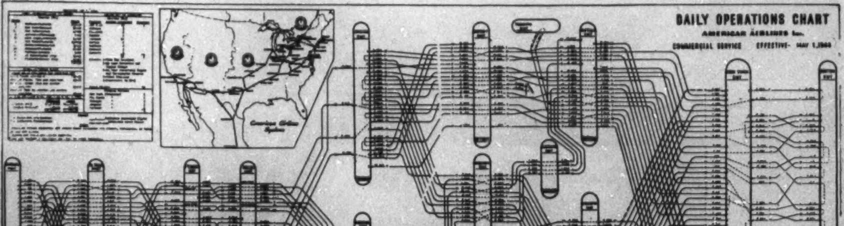
The actual overhaul procedure to which each unit is subjected is determined largely by the service experience with the unit. However, in many cases it corresponds 100 per cent with the manufacturer's service instructions. Figures 5 through 13 illustrate typical overhaul procedure on a number of electric items.

After approximately 6,500 hours and not more than 8,000 hours of operation the airplanes are brought in to New York for a ship overhaul. In addition to all the structural inspection and repair the remainder of the electric system, namely, conduit, wiring, junction boxes, switches, and so forth, is inspected and replacements are made where necessary (Figures 14 through 23). In general the aircraft conduit and wiring in the engine nacelle compartment must be changed at this time. The fuselage conduit and wiring, however, has been found in satisfactory condition after 18,000 hours (approximately five years) of service. The wire in the engine section, commonly referred to as the engine harness, must be replaced at each engine-overhaul period.

Up to this point we have discussed the ideal maintenance procedure in regard to inspection and replacement of equipment for overhaul. Supplementing this problem is another in which provision has to be made for replacement of units which fail in service. Briefly the problem is one of stocking a sufficient number of spare units at strategic stations, the number of units and stations being determined from past experience, so that flight schedules will suffer a minimum of interruption. In addition some means must be provided for returning the used part to New York where it will be overhauled and returned to service. In order to expedite the movement of these parts, it is necessary to ship 60 per cent of them by airplane to New York. It might appear on the surface that the airlines are saving themselves a transportation bill; however, such reasoning is fallacious inasmuch as the present operation at high-load factors places a penalty on any additional weight which does not result in an increase in revenue. American Airlines estimates the addition of one pound of such weight to cost approximately $88 per airplane per year.

It is because of the necessity for replacements of units in the field that the airlines have placed so much emphasis upon design of equipment and installations for ease of maintenance. It is important to note that design for easy servicing requires the co-ordination of both the equipment and the aircraft manufacturer. That is, an aircraft accessory can be designed with the most effective quick disconnect features, but, if it is so installed on the aircraft that it requires a removal of stress plates, miscellaneous lines, and other items of equipment before it can be reached, it is obvious that the maintenance problem has not been satisfactorily solved (Figure 24). The

Figure 25. American Airlines' daily operations chart



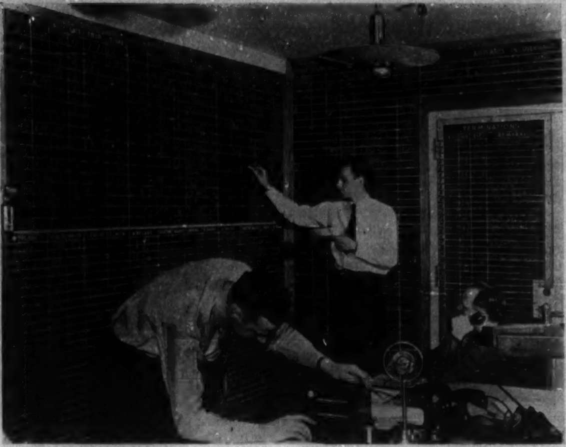


Figure 26. Airplane routing office

equipment manufacturer owes it to himself to follow up on the installation of his equipment in order to insure that the installation of his product is not unfavorable to its ready servicing. This is particularly important when the unit of equipment is of such nature that it requires frequent servicing. We might cite as an example the spark plug for which an installation was designed requiring the removal of considerable cowling. Removing one spark plug necessitated approximately nine man-hours of labor. As a matter of fact it is this type of design which is giving impetus to the creation of readily removable engine-mount assemblies, since it appears that it would be quicker to change the entire engine than it would be to change the spark plugs.

The entire maintenance system would be impossible without a smoothly running record and report system. The goal of this system is to record all changes and inspections made to the airplane and its parts so that it is possible to determine at any time when a part was removed or installed on an airplane; its identifying serial number; the mechanic who replaced the part; and the complete overhaul history of the unit, including the trouble found, correction made, and the mechanic who did the work. Some idea of the scope of this system can be gathered from the fact that there are some 65 parts in each airplane of which overhaul records are kept.

Equally important is the job of the maintenance dispatch office which has the responsibility of controlling the scheduling of airplanes so that they will be at their proper stations within the designated limits of time for inspection periods, engine-change periods, and overhaul periods. Some idea of the scope of this job can be obtained from Figure 25 which illustrates the various scheduled routes over the system. Each base station furnishes a daily report by radiogram listing the scheduled routing of all airplanes in its individual control sector. This information includes data on engine time since overhaul and time since the last inspection of the airplane. From this mass of data the maintenance dispatcher issues routing instructions which are designed to bring the airplane to an appointed station in time for this required servicing (Figure 26). In spite of the complexity of the system, the average engine-change time of 42 engines for the month of April 1944 was exactly 750 hours.

This discussion would not be complete without a word about the activities of the airline's engineering department and a brief description of its functions. In a broad sense the engineering department acts as a technical consultant to the maintenance department with the object of improving the service life of the equipment and thereby increasing the efficiency of the operation. A service-report system is utilized to keep both the maintenance and engineering departments informed of the current difficulties. These reports are initiated in the field at the time the trouble is experienced and include all pertinent information which will be of value in the determination of suitable corrective action.

Not only is the existing equipment under constant investigation for possible improvements, but also a weather eye is kept on the industry at large to utilize the fruits of improved design in order to obsolete less satisfactory equipment. It is the engineering department's responsibility to maintain contact with all parties concerned in the design and manufacture of the aircraft and to negotiate desirable improvements in any of the associated equipment. The organization of American Airlines' engineering department is shown in Figure 27 and illustrates the degree of specialization which is necessary in order to adequately cover the broad field of engineering which makes up the modern air-transport system.

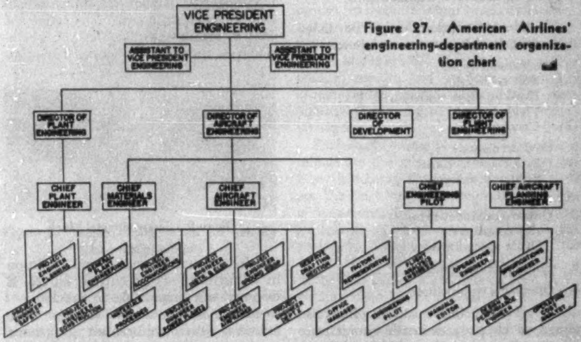

VICE PRESIDENT ENGINEERING
ASSISTANT TO VICE PRESIDENT ENGINEERING
ASSISTANT TO VICE PRESIDENT ENGINEERING
DIRECTOR OF PLANT ENGINEERING
DIRECTOR OF AIRCRAFT ENGINEERING
DIRECTOR OF DEVELOPMENT
DIRECTOR OF FLIGHT ENGINEERING
CHIEF PLANT ENGINEER
CHIEF MATERIALS ENGINEER
CHIEF AIRCRAFT ENGINEER
CHIEF ENGINEERING PILOT
CHIEF AIRCRAFT PLANNING ENGINEER
PROJECT ENGINEER PLANNING
GENERAL PLANT ENGINEERING
FACTORY REPRESENTATIVE
OPERATIONS ENGINEER
SPECIFICATIONS ENGINEER
PROJECT ENGINEER SAFETY
PROJECT ENGINEER CONSTRUCTION
MATERIALS AND PROCESSES
PROJECT ENGINEER POWER PLANTS
OFFICE MANAGER
ENGINEERING PILOT
MANUALS EDITOR
OPERATING COST ANALYST

Figure 27. American Airlines' engineering-department organization chart

It is hoped that the foregoing discussion will have served its purpose of bringing home to the aircraft electric-equipment designer a picture of the end use of his product. More important, however, is the designer's recognition of the fact that his equipment must fall into a predetermined pattern of inspection and overhaul procedures which are not necessarily set up to accommodate his particular equipment but rather to provide an economical method of insuring the airworthiness of the complete airplane.

Electric Connections on Aircraft

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MEMBER AIEE

L. A. TAYLOR
NONMEMBER AIEE

AN electric system is as good as its electric connections. Poor electric connections on aircraft easily can cancel the tremendous advantages obtained by the use of electric systems. Little attention is given to this unglamorous but vital subject because of the more absorbing and interesting details of the electric system as a whole. Only after an unexpected and disconcerting electric-connection failure in service does the engineer realize its importance. Such failures on domestic appliances are not generally serious but on aircraft may involve many lives, much valuable equipment, and, in fact, the success of our war effort.

This paper describes factors and design details which must be considered in the design of successful electric connections on aircraft.

Types of Electric Connections

Electric connections may be listed according to their service as follows:

1. Permanent type.
(*a*). Soft soldered.
(*b*). Hard or silver soldered.
(*c*). Welded.
(*d*). Crimped.

2. Semipermanent type.
(*a*). Screw and terminal.
(*b*). Stud and terminal.
(*c*). Special.

3. Quick disconnect type.
(*a*). *AN* connector.
(*b*). Other special types.

4. Sliding connections.

5. Operating connections.

The effects of different kinds of metals, alloys, or electrolytes in the circuit usually can be neglected except for thermocouple and other similar low-voltage circuits. These exceptions require connections to be made with like materials, or they may require a design in which paired junctions are always at the same temperature. For example, a soldered joint making a connection between two copper wires will cause little difficulty when the two junctions are close together, except in the most critical circuits. Clamped or welded connections between like materials are required where undesired thermal electric voltages are to be avoided.

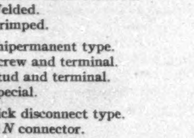

Figure 1. Flame-welded connection between two enameled wires with a twisted portion for mechanical support

PERMANENT TYPES

Soft Soldered. In spite of the fact that the art of soft soldering electric connections is relatively old in the electrical industry, it still must receive constant attention in production lines beginning at the point where the insulation is stripped from the wire.

The preliminary insulation stripping operation preferably should be accomplished without nicking, swaging or scraping any strands in the wire. The insulation on aircraft wire per *AN-J-C*-48 should be cut evenly, leaving the wire clean and free from insulation. For high-quantity production, automatic machines have been devised for cutting the wire to length and removing the insulation from the ends. Because of variations in wire size and stranding and variation in adjustment of the cutting knives, it is practically impossible to prevent occasional nicked or scraped strands. The following rules have been set up in some production lines to aid in inspecting stripped wires:

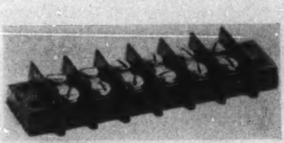

Figure 2. Screw terminal block

1. Not more than ten per cent of the number of strands can be nicked.

2. Strands which are scraped or swaged not more than 25 per cent of the diameter are acceptable.

3. If it is assumed that the nicked strands may break during assembly, the following maximum number of broken strands after assembly are allowable:

Strands in Wire	Maximum Allowable Broken Strands
7	1
10	1
16	1
19	2
26	2

Hand-operated wire insulation strippers may be used for laboratory purposes, service work, and so forth, but are subject to the same dangers of nicking and scraping.

The design of the wire insulating layers can have appreciable effect on the ease of stripping. The first or separator layer can be made of material such as number 400 electrical tape which will rupture readily and allow the other insulating layers to slide off the wires easily when they are cut. The stripping knives then can be set to the outside diameter of the wire plus the separator layer which reduces the possibility of damaging the wire.

Insulation also may be removed by burning with electrically heated blades at the desired point, allowing the insulation to cool, and pulling the insulation off the end of the wire. The interval for cooling is necessary so that hot and softened insulation, such as Flamenol, will not be forced between the strands.

Enamel can be removed safely from single fine wires, 0.004 inch or smaller by rubbing with fine emery or crocus cloth. Chemical means may be used to remove the enamel, provided the chemical is subsequently thoroughly washed off. The enamel on stranded enameled wire may be removed by heating the wire and dipping the hot portion into denatured alcohol.

After the insulation has been removed from the end of aircraft wire (per *AN-J-C*-48), it is preferable to twist the strands tightly together in a production twisting machine. They are then dipped into a rosin and alcohol soldering flux and into a solder pot. This extra "tinning" operation saves much time in a production line because the strands of the wire stay in place while the mechanical connection is being made.

When making connections between wires and vacuum-tube socket or similar terminals, the wire should be looped through eyelets or slots and wrapped around to give a mechanical support before soldering to the terminal.

The soldering operation is completed by applying flux, heat, and solder to the wire and terminal so that the solder wets the metal surfaces. The joint must cool so that the solder freezes solid before any strain is applied to the joint. Any relative movement between the wire and terminal during the freezing renders the connection mechanically and electrically weak.

Rosin or rosin in alcohol is the only flux which has proved satisfactory for aircraft electric connections. The minimum amount of flux should be used. Rosin-cored wire solder having rosin in

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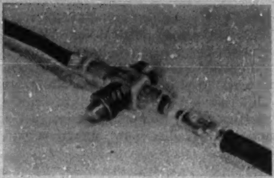


Figure 3. British wire terminal having combination screw, crimped, and clamped features

five per cent or less of the cross-sectional area can be used conveniently. When soldering near electric contacts, care must be used to prevent the rosin from splattering or running onto the contacts. Excess rosin may flake off onto contact surfaces after it has dried out, which is an important reason for using the minimum flux. The soldered connection may be cleaned with alcohol to remove all excess rosin, or it may be coated with a colored lacquer or Glyptal paint to prevent corrosion and flaking of the rosin.

Excess solder on a joint is not only wasteful, but also may cover a poor joint. A beautifully rounded ball of solder on a joint is to be avoided. The form of the strands of wire should show on a well-soldered joint, with fillets of solder between strands and terminal indicating a thorough wetting by the solder.

Hard or Silver Soldered. This type of connection is normally used only on circuits which are subject to temperature higher than the softening point of the soft solders. It is very important that the fluxes be thoroughly removed to prevent corrosion in service.

Welded. Both spot and flame welding are used extensively for special connections. Spot welding requires a very high grade of control to insure satisfactory joints because it is difficult to determine the quality of the joint by visual or nondestructive means.

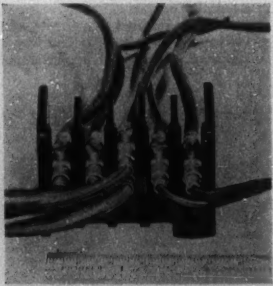


Figure 4. British terminal block using wire terminals having combination screw, crimped, and clamped features

One successful production method of connecting copper wires larger than 0.010 inch consists in twisting the wires together for mechanical support and melting the ends of the wire down to a ball with a hot flame as shown in Figure 1.

With Formex or enamel insulated wire, it is not necessary to remove the insulation when using this method.

Crimped. Several types of crimped connections have been developed for use between wires and terminals. These have the advantages of eliminating flux and solder troubles and speeding up production. Some types are crimped on both the wire strands and insulation to form a very good mechanical support. Standard crimped or solderless types for aircraft use are specified under Army-Navy Aeronautical Standard *AN*-659. One new type recently put on the market has an insulating coating on the crimped portion which eliminates the necessity of covering the terminal shank with an insulating sleeve.

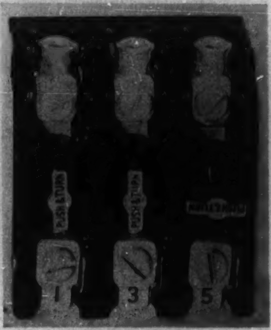


Figure 5. British terminal block for quickly disconnecting circuits

The quality of the connection obtained with solderless terminals is difficult to determine by visual inspection. The size of the terminals and crimping tools must be held to close tolerances which have been proved to produce satisfactory joints by destructive tests. The crimping pressures and the physical characteristics of the crimped tube must be carefully controlled. One type of crimping tool cannot be used with all makes of terminals because of differences in size and design. The National Aircraft Standards Committee and Army and Navy are taking steps to obtain standardized types and tools, but at the present time best results are obtained by using the tools and methods according to the terminal manufacturers' recommendations.

SEMIPERMANENT TYPES

Screw and Terminal. Screw terminals are used where space is restricted, and the currents are limited to about 30 amperes. Figure 2 shows one style which is used extensively. The number of connections which can be made under the head of a screw is limited by the length of the screw. Generally two connections are the maximum. It is also advisable to use a split spring lockwasher between the screwhead and connecting terminal.

A combination screw, crimped and clamped terminal connection used on British aircraft, is shown in Figures 3 and 4. This design has several obvious advantages in assembly and servicing. The wire terminal is a cylindrical design which is crimped on the wire and insulation. This is held by a simple screw-clamp arrangement which can be removed from the terminal board as shown.

Another British terminal block design for disconnecting circuits quickly is shown in Figure 5. Circuit isolation for testing, repair, and so forth, may be accomplished quickly by pushing the button in the terminal block and twisting 90 degrees.

Stud and Terminal. Stud-type terminals are the most reliable of all types. They will carry the highest currents and are the least subject to vibration. This is borne out by experience on aircraft generators on which stud terminals have replaced *AN* connectors.

The National Aircraft Standards Committee has issued standards on stud connectors mounted in special panels, ranging from number 6 screw studs to three-eighths-inch-diameter studs. These are described in National Aircraft Standards 17 through 22, inclusive.

A new type of stud terminal which clamps directly on the wire strands recently has appeared on the market and is shown in Figure 6. This eliminates the necessity of applying a terminal to the wire when making a connection, but the wire would be damaged if frequent removals from the stud were required.

Special. Ground connections or frame connections which are part of an electric circuit, and radio shielded con-

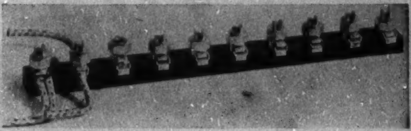


Figure 6. New type of stud terminal which clamps directly on the wire

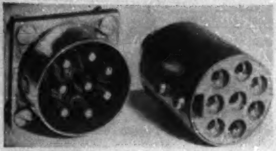


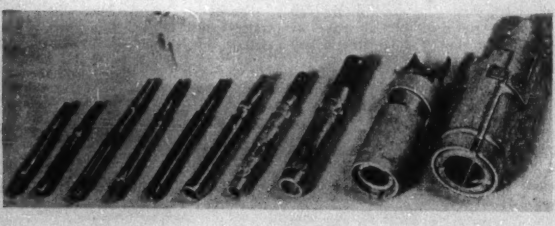
Figure 7. Pin- and socket-type connector with "banana" pin construction

nections may be classified as special semipermanent types. In addition, many moving metallic parts on aircraft must be electrically connected together with low-resistance connections in order to reduce electrical disturbances which cause noise in the radio receiver. Special bonding techniques have been developed for making these connections. A flexible stranded bare cable with crimped or soldered terminals is generally employed. The connection between the terminals and the stationary and moving parts presents problems due to the different metals and surface finishes. A stable connection with not more than 0.0005 ohms resistance is generally required. At the same time, the parts must not be subject to galvanic or normal corrosion due to climatic conditions. Protective finishes on aluminum and magnesium alloys must be removed in order to obtain the low-resistance connection, and the completed connection must be protected against corrosion. Pure aluminum washers are used between metal combinations subject to galvanic action, such as magnesium and copper. Painting the entire connection with zinc-chromate primer or similar material provides reasonable protection.

Tinned bronze lockwashers with external teeth (*AN*-936) can be used between the terminal and part to be bonded to provide a low-resistance connection by the multiple point contacts which are thereby obtained. Some practices rely on these washers to cut through insulating surfaces, such as the coating on anodized aluminum alloys, without any other preparatory cleaning of the surface.

Electric motors, generators, and other electric devices may use the frame to carry one of the circuits to a supporting part. Both mounting surfaces must be cleaned thoroughly in order to make a low-resistance metallic contact. The edges of the mounting surfaces must be protected at assembly by zinc-chromate primer or equivalent to prevent corrosion from attaching at these unprotected points.

Figure 8. Typical sockets used on *AN* connectors



The radio shielding of sources emitting strong radio interference, such as an aircraft-engine ignition system, presents a connection problem which requires accurate mechanical positioning of the parts in contact and joints that are watertight. Plain flat surfaces at the joints provide openings between the bolts a few tenths of a mil wide through which radio noise leaks. A continuous electric contact completely around the joint has been found necessary to keep radio noise contained within the ignition system. This can be accomplished by cutting a square groove in one joint surface and inserting in that groove a round resilient braided wire-covered gasket. When the joint is assembled the braided gasket is forced to contact the flat surfaces by the resilient material inside the braid. A second method for obtaining continuous electric contact around a joint makes use of a flat gasket consisting of wire screen impregnated in Neoprene. This gasket has been found to be less effective than the round braided gasket and leaks radio noise under certain conditions.

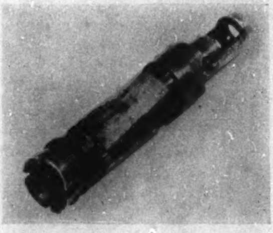


Figure 9. Socket used on Japanese connector

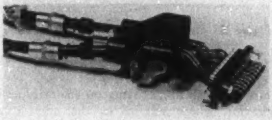


Figure 10. Disassembled German 20-circuit connector

QUICK-DISCONNECT-TYPE CONNECTORS

When electric apparatus must be quickly and frequently disconnected, special quick disconnect types are used. Some degree of reliability is usually sacrificed to obtain the quick disconnect feature. Numerous and varied designs have been made since the beginning of the electrical industry. In an effort to standardize, the joint Army–Navy specification *AN*-9534 was issued. To supplement this specification and obtain improved designs, specification *AN-W-C*-591 was issued and is under revision at the present moment. Before discussing the merits of the *AN* connectors it might be well to consider basic designs.

Simple Direct-Pressure Type. This type consists fundamentally of two metallic parts which are held together by direct pressure to make the circuit. Generally three points of contact between the metallic surfaces carry most of the current. The contact surfaces easily may be inspected and cleaned. Dirt may come between the surfaces and prevent electric contact.

Pin and Socket Type. This type consists of a pin which slides into a socket and incorporates means for maintaining radial pressure between the pin and socket. The pin can be inspected and cleaned without difficulty. The socket surfaces are difficult to inspect, but they may be cleaned by special means.

In a few designs the spring pressure is obtained in the design of the pin, as for example, the "banana" pin construction shown in Figure 7. A split pin construction also has been used but is weak mechanically.

The most common types of pins and socket connections use solid pins and a socket which, by its design, applies spring

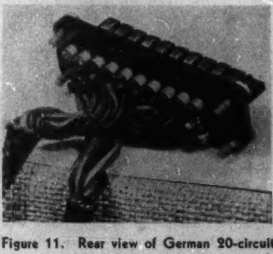


Figure 11. Rear view of German 20-circuit connector plug showing connection terminals

pressure to the pin. A wide variety of socket designs have been developed, a number of which are shown in Figure 8. A Japanese socket design is shown in Figure 9. This was a split-screw machined socket with the addition of a helical spring at the end of the socket and a *C*-shaped cylindrical spring on the body. Our experience in the operating field has shown that the use of these designs should be limited to circuits carrying less than 60 amperes. A discussion of *AN* connector design will be given later in this paper.

Sliding Contact Type. A connector design found on recent German aircraft is shown in Figures 10, 11, 12, 13, and 14. A pin is molded in the surface of the receptacle which makes a sliding contact with a flat type of spring contact on a molded plug. This design is one of the few in which the connecting surfaces easily can be inspected and cleaned. The receptable plugs are held together by a simple toggle spring arrangement, as shown, and a synthetic rubber gasket provides a tight seal to prevent water from entering.

AN CONNECTORS

At the time this paper was written, *AN* connectors were described in specifications *AN*-9534*a* and *AN-W-C*-591. It is assumed that the reader is familiar with these specifications. Our experience in using *AN* connectors made by a wide variety of manufacturers, according to these specifications, in all types and sizes, indicates that the specifications are woefully inadequate for the insurance of obtaining satisfactory *AN* connector performance. The successful operation of *AN* connectors in the combat field has been due mainly to the choice of available connector designs and extra inspection before assembling in equipment.

One of the weakest parts of *AN* connector design from practical considerations has been in the socket. A satisfactory socket design must be one which cannot be damaged by inserting the pin at any angle which may be obtained during the process of mating the parts. It should not be damaged by test pins such as are used on circuit testing instruments.

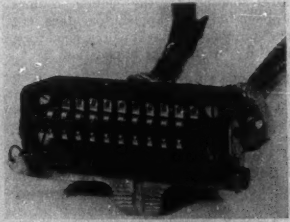


Figure 12. Plug-side view of German 20-circuit connector

Several successful designs use a solid ring at the entrance end of the socket to guide the pin into place (see Figure 8). An oversize test pin cannot be inserted in such a socket. Other successful types use auxiliary springs and bands to maintain positive contact. The sockets may be inserted in a molded phenolic insulator which limits the possible motion of the sprung sections of the socket so that they do not go beyond the elastic limit.

Adequate locking means to hold the plug and socket together are needed. Safety wire through a hole in the coupling nut to a mounting screw is very effective.

Complete 100 per cent inspection of *AN* connectors is imperative. Inspection details to be noted may be listed as follows.

CONNECTOR SHELLS

1. The shells shall have a good appearance with no porosity, blow holes, burrs or cracks. All sharp corners shall be broken with a $^1/_{16}$-inch radius unless otherwise specified.

2. The shell shall be of sufficient thickness so that the connector will be plugged to its mating unit in the correct position only.

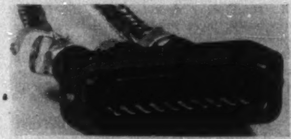


Figure 13. Socket of German 20-circuit connector

3. The shell tongue shall have enough thickness and strength so that it will not break in use and will allow the connector to be plugged to its mating unit in the correct position only.

4. The shell, when spun over the phenolic insert, shall be so spun that stresses are not set up in the insert which cause the insert to break or shear.

5. Information stamped on the shell shall be correct and easily legible.

6. The snap-ring slot in the shell or in the coupling nut shall have square sides with sufficient width and depth so that the snap ring cannot pull, slide, or twist out of the slot during use of the connector. A special slot which wedges the snap ring against the coupling nut when the nut is tightened has been found to be very effective with some designs.

7. The shell threads shall be centered properly on the shell so that they are not too deep on one side and too shallow on the opposite side. The threads shall not be broken nor contain broken-out sections, and the threads on half of the shell shall be in complete alignment with those on the other half. Burrs on the threads at the casting parting line shall be removed.

8. The threads on all connectors shall be bare, except for antiseize compound, and shall have no varnish, shellac, paint, or anodic treatment.

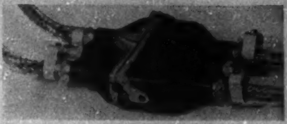


Figure 14. Assembled German 20-circuit connector

9. The antiseize compound on the threads shall not contain grit and shall conform to *AN* specifications.

10. Flanges on the inner parts of the shell shall be square and shall project sufficiently to prevent an inner shell or phenolic insert from pushing or pulling out of the shell.

11. The connector having split shells shall be so held together by sufficient supports, and so forth, that the inner shell or phenolic insert cannot be forced out of the connector.

12. The coupling nut shall be so fastened to the shell that it can be tightened thoroughly with pliers without pulling from the shell.

13. Springs on split-shell connectors shall be of sufficient strength so they do not break nor slide out of position.

PHENOLIC INSERTS

1. The phenolic material shall not be brittle. The pins and sockets shall not push out of the phenolic backing disk nor pull through the front of the phenolic insert during plugging and unplugging of the connector. The phenolic guide tongues, slots, edges, phenolic material between contacts, letters, shoulders, flanges, and ridges shall not fracture during connector use.

2. The phenolic material shall be an insulating material and must not contain metal dust, metal chips, or other conducting material that will allow short circuits between contacts or between contacts and shell.

3. The phenolic insert shall be so designed and built that all contacts are located properly according to *AN* drawings, and all pins of the same size must protrude the same amount inside the shell.

4. The phenolic backing disk shall have a slot fitting to a tongue in the shell, or vice versa, which will make it possible for the backing disk to be assembled in the correct position only.

5. The manufacturer of the connector shall assemble the phenolic insert of the socket-type connector with the backing disk of the socket-type connector, not with the backing disk of the pin-type connector, and vice versa. When the backing disks are incor-

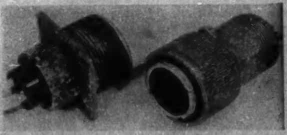


Figure 15. *AN* connector after 96-hour humidity test

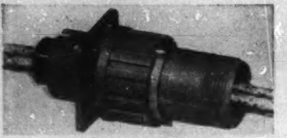


Figure 16. Sample Monowatt Electric Corporation *AN* snap-in-type connector

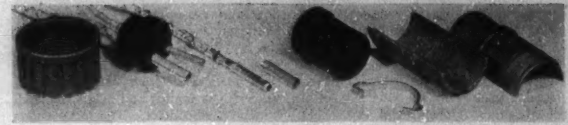


Figure 17. Sample Monowatt Electric Corporation *AN-3106-18-5S* snap-in-type connector disassembled

rectly assembled, the connector lettering is reversed.

6. The tongue on the phenolic insert shall be large and strong enough so it cannot shear or break off. This difficulty is still being experienced on size ten connectors without supporting shells on phenolic inserts.

7. The holes for the metal sockets in the phenolic inserts shall be of such size and shape that the metal socket can readily accommodate itself to the pin without the pin sliding down the outside of the socket.

8. With the connectors of the molded in pin design, the phenolic material must not shear from the pin shoulders and allow the pins to push through the sockets, for all pressures on the pins up to ten pounds.

9. The lettering at the front and rear of the pin insert must coincide with that at the front and rear of the socket insert.

10. It has been found desirable to have all the solder cups face in one direction.

11. The phenolic insert is not to push nor pull out of the connector shell during plugging or unplugging of the connector with its mating part.

CONTACTS

1. The pins must be of a strong durable material that will not bend nor break because of possible forcing or twisting when the connector is plugged or unplugged to its mating part.

2. The pins shall have a durable plating of sufficient thickness to prevent the formation of corrosion.

3. The pin shoulder diameter shall be large enough so that the pin will not push through the phenolic backing disk when the connector is plugged or unplugged to its mating unit.

4. The solder cup shall not break off the contact during normal use of the connector.

5. No pin is to push out through the backing disk with a pressure of ten pounds applied to it. (At General Electric Company's request the American Phenolic Corporation was set up to apply a ten-pound pressure test to each pin in the connector and mark each connector so tested with a green dot. This test increases the connector price, but the improved quality has been justified.)

6. The socket design shall be such that the sockets will not be damaged by any possible twisting or forcing when the connector is plugged to its mating unit or when test pins are inserted into the sockets. Sizes 16 and 20 sockets made by the American Phenolic Corporation, and sizes 12, 16, and 20 sockets made by the Monowatt Electric Corporation have reinforcing sleeves made of durable bronze not less than eight mils thick. (At General Electric Company's request, on and after October 23, 1943, Amphenol and Monowatt supply General Electric only with connectors that have been given an extra 100 per cent inspection at the manufacturer's plant to make certain that all small-size sockets, as mentioned previously, have reinforcing sleeves. The connector manufacturer shows this extra 100 per cent inspection by a red dot on the connector. This inspection should be applied to all banded-type sockets made by all manufacturers.)

7. The socket should be so constructed that liquid solder in the solder cup cannot leak through to the front of the socket and prevent insertion of the pin.

8. The hole in the socket should be of such shape that the pins readily slide into the sockets the first time.

9. The insert socket holes should be of such size that the sockets are free to adjust themselves to the pins when the connectors are plugged. It is preferred to have the pins fixed and the sockets floating.

When *AN* specifications are revised, it is hoped that some identifying means will be devised so that connectors according to the revised specifications can be distinguished from older types.

Corrosion protection of the *AN* connectors is not required in existing specifications. Figure 15 shows the corroded condition of an *AN* connector shell after exposure to 100 per cent humidity for 96 hours at 33 degrees centigrade. The threaded portions which were covered by antiseize compound are in perfect condition. In tropical and salt-water service, connector shells, pins, and sockets must be coated with a protective grease, for example, lanolin, to insure continued operation. This results in a very messy condition, as dust and dirt is picked up on the grease.

A new *AN* connector design with provision for safety wiring the hold nut and other advantageous features is shown in Figures 16 and 17. Connections may be made between the pins and wire by crimping, soldering, or both. Holes in the crimped tube opposite the crimp permit inspection of the crimping. The design of the insulating parts gives greater creepage distances and mechanical support to the wires without the use of insulating sleeving.

Under desert conditions, dust in the coupling nuts on *AN* connectors causes the threads to jam. The German connector design shown in Figures 10 through 14 is not affected by dust and can be disconnected quickly under any conditions. It is hoped that this advantageous characteristic may be incorporated in a future *AN* design.

SLIDING CONNECTIONS

The design of sliding connections is an uncompleted art. The sliding connection may be on a resistor, a commutator, or a slip ring. The choice of materials and design is controlled by many factors and variables which are not thoroughly understood. With their use on aircraft, the additional effect of high altitudes must not be overlooked. Not so long ago we were astonished when our ordinarily good carbon brushes wore out on commutators in a matter of ten minutes or less. A great deal of research effort has been required to overcome this difficulty. Today designs can be made which will operate under such conditions. The details naturally cannot be discussed because of wartime restrictions.

It is safe to say, however, that each new application must be tested thoroughly under all operating conditions. The same solution to the sliding contact problem on one application does not necessarily mean that it will be good for a new slightly different application.

OPERATING CONNECTIONS

Operating connections are normally used for control purposes. They make or break circuits carrying electric power at various voltages and direct or alternating load currents. They range from hand-operated switches to cam-operated magneto contacts.

Again it is very important to check operating connections with the exact loading conditions under which operation

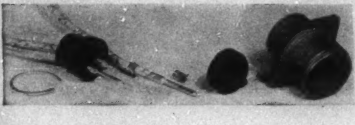


Figure 18. Sample Monowatt Electric Corporation AN-3106-18-5P snap-in-type connector disassembled

is required. Equivalent circuits often are misleading in action.

With the increased use of electric equipment on aircraft, relay contacts have been designed to carry currents in the order of thousands of d-c amperes at 24 to 30 volts. Operating contacts for 400-cycle a-c circuits at 208 volts are under development.

Conclusions

The vital importance of electric connections on aircraft is not generally appreciated by people accustomed only to the connections on home appliances. A poor connection on an electric toaster blows a quickly replaceable five-cent fuse, and the lady of the house has to endure the hardship of eating untoasted bread until the handy man makes a repair. One poor connection on an aircraft may effect the lives of many people and cause damage in the order of hundreds of thousands of dollars.

An engineer or production man may be proud to have failures due to poor electric connections on home appliances reduced to one per cent. But, if this percentage were allowed on each of the types of electric equipment on an airplane, would he be willing to fly in the plane?

For successful modern aircraft operation, electric connections must be engineered carefully, well constructed with the proper materials, 100 per cent inspected, properly applied, and properly serviced.

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Solenoid-Operated Control Valves

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THE presentation of a paper on electric aircraft apparatus by manufacturers of hydraulic control equipment is very likely to lead to a discussion[1,5] of the relative advantages and disadvantages of hydraulic and electric systems for accessory actuation. Let us evade this controversy for the present by stating that the writers are not advocating either an all-electric or all-hydraulic system but are describing a type of device that makes possible the utilization of some of the best features of both forms of power.

It commonly is conceded that one of the strong points of electric systems is the relative lightness, cheapness, and flexibility of electric wiring over hydraulic plumbing. This is particularly true when it is required to transmit only very small power. Consequently, a system that substitutes low-energy-level electric wiring for high-energy-level hydraulic plumbing would seem to offer advantages in cost and weight for some applications, especially in the larger more advanced airplanes. For example, it has been estimated[3] that for a multiengined airplane with a hydraulic pump on each engine the use of electric remotely operated valves for controlling wing flaps reduces the length and weight of plumbing to less than one half that required for an all-hydraulic installation with direct cockpit valve control.

An additional advantage offered by this electrohydraulic system is that it makes it possible to eliminate high-potential wiring and high-pressure plumbing from the cockpit and other critical areas, thus giving a safer and cleaner installation.

Obviously, the device that makes this electrohydraulic system practicable is the remotely controlled electrically operated hydraulic valve.

General Design

The general design requirements for these electromechanical control devices are about the same as for other similar aircraft components in that they must incorporate:

1. Utmost reliability.
2. Absolute minimum weight consistent with other requirements.
3. Operation over an extremely wide range of ambient temperatures and pressures.
4. Resistance to vibration.
5. Adaptation for use in the standard aircraft electric and hydraulic systems.
6. Auxiliary means of manual operation for use in case of electric power failure. This last requirement is not always necessary.

In designing one of these control devices the first step is to decide which of the several types of electromechanical drivers and hydraulic valves is to be used. The most commonly used drivers are electric motors, electrically heated vapor-pressure motors, and electromagnetic solenoids. Similarly, the usual valve types are the well-known slide, disk and poppet.

Various combinations of these drivers and valves have been proposed for specific purposes, but the writers believe that the combination that promises to fulfill most completely the requirements for the greatest number of applications is the electromagnetic solenoid driver applied to the piston poppet-type valve. This combination has the advantages of the simplicity and rapidity of operation (less than $^1/_{30}$ second) of the solenoid and the low fluid leakage and low operating power of the poppet-type valve. These characteristics are conducive to obtaining the lightest and most universal device.

In contrast, electric motors geared to valves as drivers are characterized by relative complexity, difficulty of obtaining manual operation, high weight for low-force valves but relatively low weight for high-force valves, and an intermediate operating time of from one to several seconds. Vapor-pressure devices are inherently simple and light but slow, requiring from approximately $^1/_2$ minute to several minutes to operate, and are likely to be more vulnerable than other devices. They are also difficult to accommodate to wide temperature and pressure ranges. Similarly, slide valves usually have greater fluid leakage than other types so that they are suitable only for hydraulic systems designed especially for their use. Disk-type valves, although simple and reliable, usually require relatively high rotary operating forces which make them suitable for adaptation to geared electric-motor drive but not solenoid actuation.

This discussion will be limited to the 24-volt d-c system, since that is the one in most general use at present.

Valve Design

Hydraulic valves are rated according to the volume of fluid that they will pass in unit time with a certain permitted pressure drop. This rated flow and the

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design of the valve are the important factors that determine the force and displacement necessary to operate the valve. Two general valve designs are diagrammed in Figures 1 and 2. Cutaway illustrations of practical embodiments of the same designs are given in Figures 3 and 4.

The valve of Figures 1 and 3 is unbalanced; that is, the fluid pressure is effective on the inlet face of the poppet so that the operating force is the sum of

1. The seal frictions.
2. The product of the fluid pressure and the effective poppet area.
3. The force of the return spring.

In the partly balanced valves of Figures 2 and 4 most of the force caused by the hydraulic pressure acting on the poppet face is neutralized by reducing the effective area. This has the tremendous advantage of reducing the valve operating force as is shown by Figure 6 but the considerable disadvantage of making the manufacture more difficult and costly.

The curves of Figure 6 give the operating forces required to crack and to hold open both balanced and unbalanced poppets. These curves apply to 0.266-inch diameter poppets for three-gallon-per-minute service. The values of operating force are only approximate, since they depend to a considerable extent upon

1. Detail design.
2. Manufacturing tolerances.
3. Types of seals.
4. In the case of the partly balanced valves, upon the degree of balance that can be obtained and still maintain the fluid leakage below the rated values set forth in government specifications.

The curves will serve, however, to show the considerable savings in driver work that is made possible by using the balanced-poppet principle.

When it is considered that approximately three-quarters of the weight of an unbalanced solenoid valve is driver weight, the added difficulty of manufacture involved in the partially balanced valve is entirely justified. Also, the trend is toward higher system pressures with correspondingly higher valve operating forces which enhances the value of balanced valves.

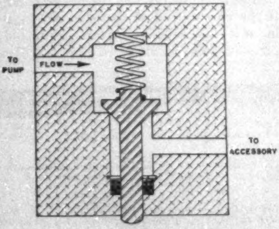


Figure 1. Diagram of unbalanced valve

This generalization in favor of partly balanced valves falls down for very small sizes. Figure 5 illustrates a cutaway model of a small shut-off valve originally intended for controlling the flow of anti-icing fluid. In this case the parts are too small and the operating pressure is too low to make a balanced construction practical. The savings of perhaps one ounce of weight out of five ounces would not be sufficient to justify the considerable added complication.

Solenoid Design

When the designer comes to the solenoid, he is in a position to take advantage of the low operating force and short travel distance that has been designed into the valve.

It would be out of place to include a detailed account of solenoid design in this paper, since this subject has been covered comprehensively in many previous publications.[3,4] Obviously, the design of solenoids for aircraft will follow the same principles incorporated in other electromagnetic devices with the exception that since weight is so very important even greater efforts than usual must be made to utilize all materials to the best advantage.

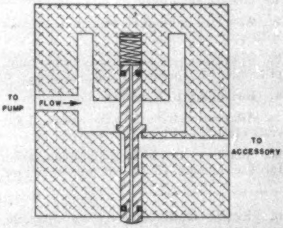


Figure 2. Diagram of balanced valve

Some of the major airlines have published statements giving a dollar cost to the weight of air-borne equipment in revenue lost in diverted pay load during the life of a commercial passenger or freight airplane. This figure averages several hundred dollars per pound for such airplanes. A similar figure for military aircraft would be impossible to calculate because of the intangible considerations involved, but on the basis alone of the lower total plane mileage for military craft compared to commercial planes, it should be several thousand dollars per pound. This cost of weight contrasts to the direct cost of weight of from four to 30 cents a pound for the materials used in usual industrial electrical equipment. Accordingly, the efforts of designers of solenoid valves and similar air-borne equipment to reduce weight to the very minimum must be multiplied in proportion.

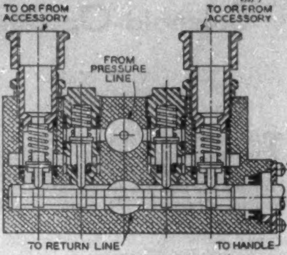


Figure 3. Section of manually operated unbalanced valve

Magnetic-circuit calculations indicate that the common iron-clad cylindrical solenoid is best adapted for this purpose. This shape also has the advantages of mechanical ruggedness, manufacturing simplicity, and relative invulnerability.

Besides the increased importance of weight there are two other design considerations which differ sufficiently from usual practice to be worth mentioning:

1. Valve operation is usually intermittent, most valves being used only once or twice for a minute or two during a flight.

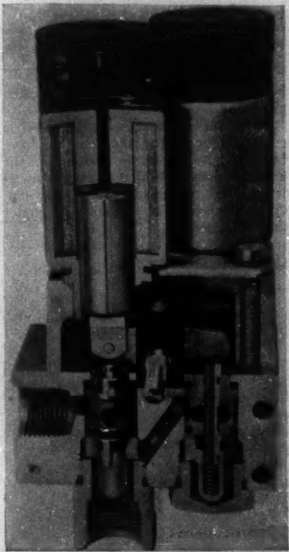


Figure 4. Cutaway model of partly balanced valve

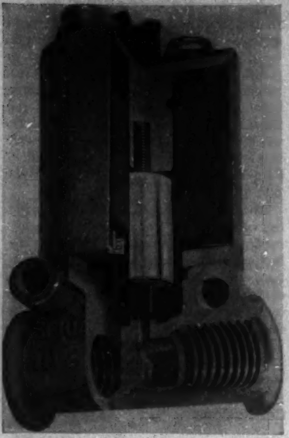


Figure 5. Cutaway model of shutoff valve

2. The expected life of this type of equipment is 20,000 operations or only a fraction of that expected of most industrial equipment. Also, electric power for the amounts and times involved is of little importance.

These considerations change the accent of the design.

One effect of these design considerations is that magnetic circuits are usually operated completely saturated. This along with the fact that the structures are small makes the problem of assuming leakage factors very difficult. As an example, a small solenoid similar to the one used on the valve of Figure 5 had nearly the same force–displacement curve without the outer iron shell as with it. This unit had been calculated carefully, but the error in assuming the leakage coefficients was so great that the outer shell had little magnetic value and, except for mechanical reasons, constituted nearly useless weight.

The engineer's first thought when confronted with the weight–duty cycle requirements along with an expected life of only 100 hours is to operate at higher than usual temperatures. Solenoids have been made using asbestos- and glass-insulated wire for operation at temperatures over 300 degrees Fahrenheit. Unfortunately, the temperature coefficient of resistance of copper is sufficiently high that the force is lowered as indicated by Figure 7 which is for an experimental glass-insulated solenoid at 0.125-inch plunger gap. When it is compared to the force–displacement curve 3 of Figure 8 for two poppets, it will be seen that a single poppet valve will operate when the solenoid is cold, will remain open for two or three minutes, and then will close automatically. After this time the valve will be entirely inoperative until it has cooled to approximately normal temperature.

Although this performance conforms with the normal duty cycle, it does not satisfy aircraft requirements in that the valve accidentally may be left in the circuit until hot in which case it will not operate if required to do so. One solution to this difficulty is to limit the temperature with a thermostatic overload switch such as is sometimes used on electric motors. A performance curve for a solenoid equipped with a thermostat switch set to open at 250 degrees Fahrenheit and reclose at 175 degrees Fahrenheit is given in Figure 9. This solenoid would operate the valve just during the time interval that the circuit was closed; during approximately 70 per cent of the time it would be inoperative. This probably would be satisfactory for many applications, if a better arrangement were not available.

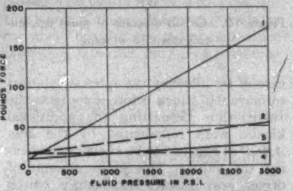


Figure 6. Hydraulic-valve-poppet operating-force curves for three-gallon-per-minute capacity (0.065-inch poppet travel)

Curve 1—Unbalanced poppet cracking force
Curve 2—Unbalanced poppet holding force
Curve 3—Balanced poppet cracking force
Curve 4—Balanced poppet holding force

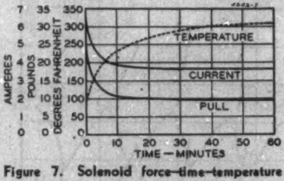


Figure 7. Solenoid force–time–temperature curves

For optimum design of the unit, the valve-operating and solenoid forces should be matched as closely as possible. This may be accomplished at least roughly by:

1. Choosing the most suitable configuration of magnetic circuit.

2. Using a double-coil winding.

3. Combinations of these two principles.

For instance, referring to Figure 8, the valve force curve 3 can be matched more closely to a solenoid curve if a high ampere-turn winding is used to give the force curve 1 with the iron circuit designed to saturate at *E*. At point *F* on the solenoid curve, the low resistance coil is switched out and a high resistance coil substituted. In contrast, a normal unsaturated solenoid designed to give adequate force at point *A* would have a characteristic curve similar to curve 4. The heating and weight of such a design would be prohibitive.

The double-coil arrangement[*] uses a high-power operating coil with comparatively few turns of heavy wire connected in parallel with a high-resistance holding coil wound with a large number of turns of fine wire. A pair of contacts operated by the solenoid plunger are connected to open-circuit the pulling coil when the plunger is at the inner limit of travel. Then, when the solenoid is first energized, the circuit resistance is low because the low-resistance coil is in the circuit so that with the plunger out the force is relatively high. When the plunger travel is completed the current is reduced by open-circuiting the pulling coil. Consequently, the force and heating are reduced.

Other circuits with somewhat different characteristics are available for accomplishing this same purpose. For instance, the two coils can be connected in series with the high-resistance holding coil short-circuited for the pulling stroke and open-circuited for the holding condition. This arrangement has the advantages of slightly lower heating while holding and improved contact conditions. The disadvantage is that the necessary inrush operating current is slightly higher. The parallel coil circuit was chosen for the solenoids described because of the lower inrush current.

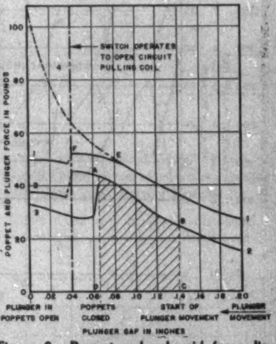


Figure 8. Poppet and solenoid force–displacement curves

Curve 1—Solenoid plunger force at 80 degrees Fahrenheit
Curve 2—Solenoid plunger force at 250 degrees Fahrenheit
Curve 3—Force for two poppets
Curve 4—Typical unsaturated solenoid plunger force at 80 degrees Fahrenheit

The solenoid designed to operate the valve of Figure 4 has a flat-ended plunger and a double coil connected as in Figure 10. This is a three-position four-connection valve using four poppets and two solenoids with each solenoid operating two poppets. The characteristic curves for 80 degrees Fahrenheit and 250 degrees Fahrenheit are given in Figure 8. The operating coil is 394 turns of number 23-gauge copper wire and the holding coil is 2,150 turns of number 32-gauge wire. The room temperature resistances are 1.90 ohms and 152 ohms respectively, giving currents of 13.7 amperes and 0.171 ampere at 26 volts. At 250 degrees Fahrenheit the force with 0.065 plunger gap is 44 pounds with approximately 46 pounds hold-in force if the operating coil is not open-circuited. With only the holding coil in the circuit the holding force is 38 pounds. With the operating coil open-circuited as in normal use but with the holding coil energized, the ultimate temperature rise after several hours is about 60 degrees Fahrenheit. If the operating coil remained in the circuit, the solenoid literally would burn up in approximately three minutes.

The solenoid furnishes a driver that will operate the partly balanced valve of Figure 4 at just 250 degrees Fahrenheit. This is 30 degrees Fahrenheit higher than the highest operating temperature of 60 degrees Fahrenheit rise over 160 degrees Fahrenheit ambient so that some reserve driver force is available. However, the works represented by the areas under the respective force–displacement curves are matched so evenly that there is inadequate reserve to insure reliable operation under adverse conditions. This is taken care of by allowing the solenoid plunger to move 0.075 inch before it contacts the valve, thus permitting the additional kinetic work represented by the area *ABCD* of Figure 8 to be effective in opening the valve. This extra amount of driver work insures satisfactory switch action and valve operation under the most adverse conditions.

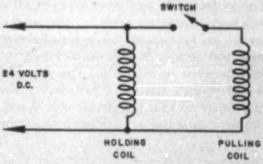
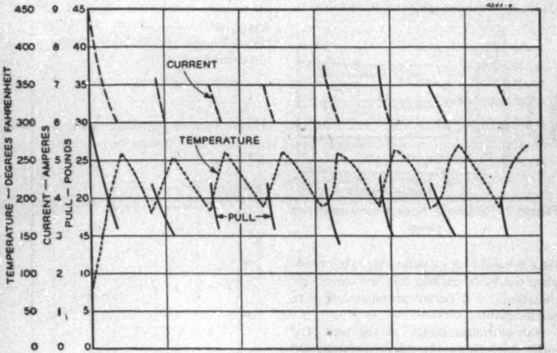


Figure 10. Circuit diagram of shunt double-coil solenoid winding

Figure 9. Force–time–temperature curves for thermostatically controlled solenoids



One interesting suggestion regarding means of reducing solenoid weight proposes to use aluminum wire instead of copper wire. Simple calculations show that in the coil itself a weight saving of approximately one half could be effected by this means. However, when the increase in weight of the enclosing iron magnetic structure caused by the larger volume of the aluminum is considered, the net difference in the weight of the whole structure is negligible. Moreover, insulated aluminum wire is difficult to obtain which is an important consideration at present.

Another argument concerns the winding of the coil. For 24-volt d-c service random winding would be entirely satisfactory, particularly with the new high-temperature nonscuffing enamels. However, the fact that approximately ten per cent more volume is required for a random winding than for a layer winding without extra insulation increases the frame weight about five per cent which is not justifiable.

Conclusions

The following general conclusions may be drawn as to the use and design of electric remotely operated hydraulic valves.

1. Considerable savings in installation weight may be effected by electric remote control of hydraulic valves, particularly when the accessory to be controlled is some distance from the cockpit.

2. It may be necessary to sacrifice simplicity and low valve cost to obtain the minimum over-all weight and dollar cost.

3. The analysis indicates that solenoid valves should be designed for low operating forces and efficient electrical and mechanical elements; not so much on account of the electric power requirements but to decrease heat dissipation and, consequently, size and weight.

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A New High-Frequency Capacitor

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Synopsis: Limitations of the ordinary capacitor at high frequencies have been in part overcome by a new three-terminal feed-through capacitor having a low impedance over a wide frequency range, thus enabling improved by-passing or filtering over this wide range of frequencies.

ONE of the common uses of capacitors is by-passing high-frequency current from circuits in which direct or low-frequency current is flowing. The frequency range over which it is effective as a by-pass depends upon the relative impedance of the capacitor and the impedance across which it is connected.

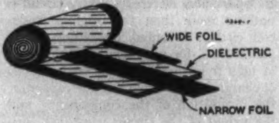

Figure 1. Isometric drawing of nearly rolled Hypass capacitor showing electrode positions

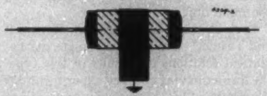

Figure 2. Completed Hypass capacitor with leads attached

The impedance of a capacitor at a given frequency is determined by its capacitance, inductance, and resistance. At low frequencies the impedance is largely determined by the capacitance, but at high frequencies and thus in most by-passing applications, lead inductance and the inherent inductance of the capacitor become the controlling factors.

Usually, capacitors for by-pass use are so chosen that series resonance occurs at the frequency where the most complete elimination of undesired high-frequency current is needed. By increasing the ratio of C to L, the frequency range over which the capacitor is effective as a by-pass can be increased. Mechanical considerations (namely, the necessity for making connections to the capacitor) and the inherent inductance of the capacitor limit the frequency range over which low impedance can be obtained by this approach.

The use of capacitors in parallel having different impedance–frequency characteristics always results in a region of high impedance where the combination becomes antiresonant. Thus, this commonly used method of by-passing over wide frequency ranges may be ineffective unless the region of antiresonance is chosen where no by-passing is needed.

A new approach to the wide-band by-passing problem is the Hypass capacitor which uses a unique design to provide an extremely broad range of low impedance extending into the ultra-high-frequency range.

Construction of the Hypass Capacitor

"Hypass" designates a three-terminal feed-through capacitor which lacks the usual resonant characteristics of ordinary capacitors. This is accomplished by operation of the capacitor as a transmission line and by a physical design which allows extremely low inductance in external connections.

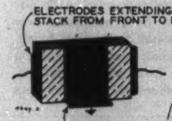

Figure 3. Stacked mica Hypass capacitor

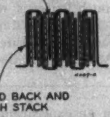

Figure 4. Cross-section of mica Hypass capacitor

The Hypass capacitor may be constructed with the same conductor and dielectric materials as conventional types, but the internal connections to the capacitor plates and the arrangements of the plates and dielectric differ from the usual type. In its usual form the Hypass capacitor has two long foil electrodes of unequal width separated by a dielectric of intermediate width. The foils and dielectric may be wound or rolled so that the wider foil extends beyond both edges of the dielectric as shown in Figures 1 and 2. The narrow foil is centered in respect to the edges of the dielectric and connected at only one end to a terminal, usually a metal container which may be grounded. Each edge of the extending wider foil is then made essentially a unipotential surface by soldering or other means by the techniques employed in conventional "noninductive" capacitors. Leads are connected to the two resulting equipotential surfaces to carry the low-frequency or direct current through the capacitor. The current-carrying capability of the Hypass capacitor may be increased by inserting an auxiliary conductor through the capacitor without affecting adversely the frequency characteristics.

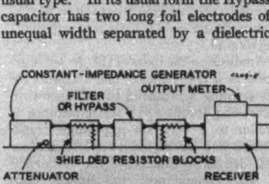

Figure 5. Insertion-loss measuring apparatus used for determining Hypass characteristics

The Hypass capacitor must be inserted in a circuit just as a low-pass filter would be connected. Its usual mechanical construction lends itself to an extremely low-inductance connection to ground while inductance in the other connections enhances the filtering and thus the by-passing effect.

Many modifications of the construction outlined above are possible. Hypass capacitors have been constructed with flat dielectric sheets, such as mica, with the wider foil replaced by strips of foil extending through the stack and with the narrow foil folded back and forth pro-

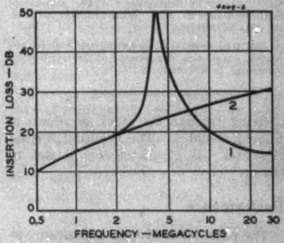

Figure 6. Insertion loss of a Hypass capacitor versus a conventional capacitor

Curve 1—Conventional 0.1 microfarad
Curve 2—Hypass 0.1 microfarad

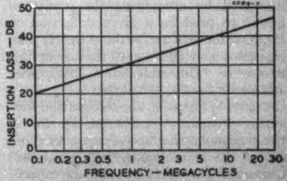

Figure 7. Insertion loss of 14.2-microfarad Hypass capacitor

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Analysis of High-Frequency Ignition Circuits

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THE modern high-voltage magnetos installed on a 2,000-horsepower aircraft engine may not resemble externally the crude induction coil used by Lenoir in 1860 to provide ignition for his gas engine. Yet, both make use of the same method to generate a transient voltage high enough to fire the spark plug. A current is first established in a primary winding surrounding a core of magnetic material and then suddenly interrupted. This interruption produces a high rate of change of flux through a secondary coil wound with a large number of turns, thus inducing a high voltage that is transmitted to the spark plug.

While their principle of operation has remained basically unchanged, the design of ignition systems has made considerable progress through the years. At the turn of the century, Simms and Bosch combined the current generator with the induction coil to make the first high-voltage magneto. Since then, new materials have become available—plastics, lightweight alloys, Alnico—and have been incorporated into improved mechanical designs to give the high-voltage magneto its present performance and reliability.

With the recent advances in aircraft-engine design, yielding higher-power outputs at higher altitudes, certain limitations of the conventional high-voltage ignition system have become of great concern to the ignition engineer. Not only is it necessary to generate and distribute higher and higher voltages at extreme altitudes, where air becomes a poor insulator, but the higher energies at the spark plug have contributed to accelerate the erosion of its electrodes and decrease its life. Fuels with high lead content, and cold running plugs, both essential prerequisites for getting more power out of a given engine, have increased the danger of spark-plug fouling, causing considerable trouble in operation.

Most of these problems have been brought under control by "fixes," and by refinements of conventional ignition systems. All persons connected with aircraft ignition have been wondering how much longer it would be possible to obtain the desired results without making a radical departure from existing systems. At the present time several manufacturers are developing new types of ignition systems that promise to get around most of the present limitations.

Low-Voltage High-Frequency Ignition Systems

Several of the proposed systems combine high frequency with distribution at low or medium voltages, offering such outstanding advantages as:

1. Operation at very high altitudes without need for supercharging.
2. Freedom from the effects of fouling.
3. Reduced plug erosion.

Most of these systems utilize the same basic high-frequency circuit: a capacitor, charged to a predetermined voltage considerably lower than the plug voltage, is suddenly discharged through the primary winding of an oscillation transformer. The secondary winding is connected to the spark-plug electrodes. This circuit is nothing more than the original Tesla-coil circuit, invented by Nikola Tesla about 1890, for producing high voltages at high frequencies. While the methods of charging and discharging the capacitor, the disposition of the components, and the values of the circuit constants vary widely from one system to another, the analysis of the basic Tesla-coil circuit seems to be of general interest, and will be the main object of this paper.

Several scientists, such as Boynton[1] and Drude,[2] have given complete mathematical solutions of circuits of this type, the former having checked experimentally the results of his theoretical investigation. However, these results may be too com-

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gressing through the stack as shown in Figures 3 and 4. The resulting Hypass capacitor has characteristics similar to the previously described type.

Performance

The behavior of Hypass capacitors was evaluated by means of insertion-loss measuring apparatus developed for low-pass filter measurement. This equipment consists of a signal generator, a detector, and decoupling networks as shown in Figure 5. Insertion loss was measured by comparing the input required to produce a given deflection of the detector with and without the Hypass capacitor inserted between the decoupling networks. The impedance level was arbitrarily fixed at ten ohms.

Curve 1, Figure 6, shows insertion loss versus frequency characteristics for a two-terminal noninductive capacitor typical of the best commercially available. Curve 2, Figure 6, shows a Hypass capacitor of the same low-frequency capacitance. Figure 7 shows the behavior of an experimental Hypass capacitor of 14.2 microfarads at 60 cycles.

The Hypass capacitor derives its favorable frequency characteristics from a decrease in the effective capacitance as a function of the increase in frequency. That this effect is not due to a change in the dielectric constant of the insulation was determined by the use of mica and polystyrene with measurements between 200 kilocycles and 30 megacycles. Thus, the capacitor represented in Figure 7 still behaves as a capacitor at 20 megacycles, although it is equivalent at this frequency to a perfect capacitor having a capacitance only a little larger than 0.1 microfarad.

Analysis on the basis of the device operating as a transmission line accounts for the seeming decrease in capacitance with increasing frequency. Its impedance is similar to a very low-impedance transmission line having high capacitance per unit length. Changes with frequency of the capacitance, inductance, conductance, and resistance per unit length are secondary and additional factors in determining the by-pass characteristics. Insertion loss versus frequency characteristics may be altered by changing the relative dimensions or the materials used in the Hypass capacitor. Qualitative evidence indicates that Hypass capacitors are effective for by-passing at frequencies at least 150 megacycles.

Applications

Among the obvious applications of Hypass capacitors are the suppression of radio noise over an extremely wide frequency range, the by-passing of multiband radio-receiver and transmitter circuits, and the use of Hypass capacitors as circuit elements in conventional filters and networks to obtain unique characteristics. Like low-pass filter sections Hypass capacitors may be connected in cascade with the marked improvement in filtering or by-passing behavior. Hypass capacitors may in certain cases be connected in parallel apparently without the troublesome antiresonance effects obtained as described in the introduction.

plete to be of much practical use to the engineer, at least in their original form. An "exact" formula, taking into account all parameters, can at best be used to calculate the performance of a device that has already been designed. The engineer, starting from scratch and faced with the problem of designing the device the best he knows how, would prefer to proceed by steps: as a starting point, a rather oversimplified theory, taking into account only the most fundamental factors, but easy to visualize, will allow him to study the basic limitations of a design, and see what they mean in terms of inches, materials, wire sizes, and numbers of turns. Once a preliminary layout has been made, secondary factors can be introduced one at a time as simple corrections, each one giving a closer approximation to the final result. It may be objected that such an approximate method is less accurate than an exact method. But an exact mathematical method, applied to an engineering problem, is only as good as the unavoidable assumptions on which it is based, and certainly not beyond the accuracy of the data. The main advantage of a theoretical analysis resides in its ability to break down a complex problem into its simplest elements or notions, simple enough so that each one can be visualized. This is nothing more than the method advocated by Descartes, pioneer of analysis and visualization.

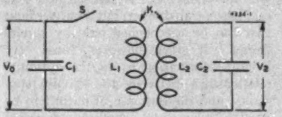

Figure 1. Basic oscillation transformer circuit

Analysis of Circuit Without Losses

This section is a mathematical study of the transient generated by discharging a capacitor C_1, charged initially to a voltage V_0, through the primary winding of an oscillation transformer, the secondary winding being connected to a capacitive load C_2. Let L_1 and L_2 be the inductances of the primary and secondary circuits respectively and K the coefficient of coupling between the two windings, defined as $K=L_{12}/\sqrt{L_1L_2}$, where L_{12} is the mutual inductance between the circuits. All losses will be neglected and all circuit elements assumed to be linear (C_1, C_2, L_1, L_2, L_{12} constants). C_2, the secondary load, represents the sum of the plug capacitance, lead capacitance, and a lumped capacitance equivalent to the distributed winding capacitance. Figure 1 shows this circuit, the device for discharging the capacitor C_1 being represented schematically by a switch S.

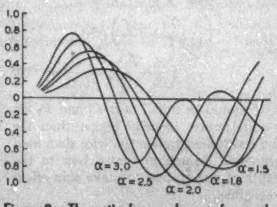

Figure 2. Theoretical wave shapes of secondary voltage

The final purpose of the ignition system is to charge C_2 to a voltage high enough to break down the spark-plug gap, and this should be done as efficiently as possible: the circuit should be proportioned so that a large percentage of the energy initially stored in C_1, is transferred to the secondary capacitance C_2. The minimum electric power required to supply ignition to a 2,000-horsepower engine is equivalent to somewhat less than 1/100 of a horsepower, so that, even with a very low efficiency, an ignition system would decrease the available engine power only by a negligible amount. However, if more joules must be generated per second in the ignition system, the generating units must be bigger and heavier, and weight is at a premium on all aircraft equipment.

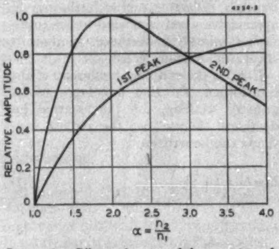

Figure 3. Effect of ratio of frequencies of components upon first and second peaks of secondary-voltage wave

An ideal equivalent circuit as described in the foregoing is, of course, a pure abstraction, but its practical interest is undeniable: the values of circuit constants that will give optimum operation (highest efficiency of energy transfer) in this circuit are good approximations of the values for optimum operation in an actual case, where the output is reduced by unavoidable losses.

The differential equation of this circuit set up in the usual manner can be brought to the quadratic form:

$$(L_1L_2-L_{12}^2)p^4+\left(\frac{L_1}{C_2}+\frac{L_2}{C_1}\right)p^2+\frac{1}{C_1C_2}=0 \qquad (1)$$

Regardless of boundary conditions, it shows that all natural oscillations of this circuit include components of two frequencies n_1 and n_2 determined by

$$4\pi^2n_1^2=\frac{\dfrac{1}{L_1C_1}+\dfrac{1}{L_2C_2}-\sqrt{\left(\dfrac{1}{L_1C_1}+\dfrac{1}{L_2C_2}\right)^2-4\dfrac{1-K^2}{L_1C_1L_2C_2}}}{2(1-K^2)} \qquad (2)$$

$$4\pi^2n_2^2=\frac{\dfrac{1}{L_1C_1}+\dfrac{1}{L_2C_2}+\sqrt{\left(\dfrac{1}{L_1C_1}+\dfrac{1}{L_2C_2}\right)^2-4\dfrac{1-K^2}{L_1C_1L_2C_2}}}{2(1-K^2)} \qquad (3)$$

These two frequencies are different from the natural frequencies of the two circuits taken separately and always frame them.

In order to determine the amplitudes and phase angles of these two components, the boundary conditions should be introduced. If it is assumed that C_1 to be charged to a voltage V_0 for $t<0$, and the switch S to be closed at $t=0$, the expression for the voltage across the capacitor C_2 is

$$v_2=\frac{L_{12}C_1V_0}{\sqrt{(L_1C_1-L_2C_2)^2+4K^2L_1C_1L_2C_2}}\times(\cos 2\pi n_1t-\cos 2\pi n_2t) \qquad (4)$$

Under no conditions can v_2 be larger than

$$V_2=V_0\sqrt{\frac{C_1}{C_2}} \qquad (5)$$

as, for $v_2=V_2$, all the energy initially stored in C_1 has been transferred to C_2, so that the currents through L_1 and L_2 and the voltage across C_1 are simultaneously zero.

The quantity v_2/V_2, representing the ratio of actual secondary voltage to maximum possible voltage, is thus representative of the efficiency of the circuit in converting electrostatic energy in C_1 into electrostatic energy in C_2. If $m=L_1C_1\div L_2C_2$, this ratio can be written

$$\frac{v_2}{V_2}=\frac{1}{\sqrt{1+\dfrac{(m-1)^2}{4K^2m}}}\times\frac{\cos 2\pi n_1t-\cos 2\pi n_2t}{2}$$

$$=U\times\frac{\cos 2\pi n_1t-\cos 2\pi n_2t}{2} \qquad (6)$$

The second factor,

$$\frac{\cos 2\pi n_1t-\cos 2\pi n_2t}{2}$$

represents the wave shape of the secondary voltage. Figure 2 shows this wave shape, and Figure 3 the amplitudes of the first and second peaks of this wave, plotted against the ratio of natural frequencies $\alpha=n_2/n_1$. Similar curves could be drawn for the third, fourth peaks, and so on. For $\alpha=2$, 4, 6, etc., one of these peaks will be 1, the two components being in phase and maximum at the same time.

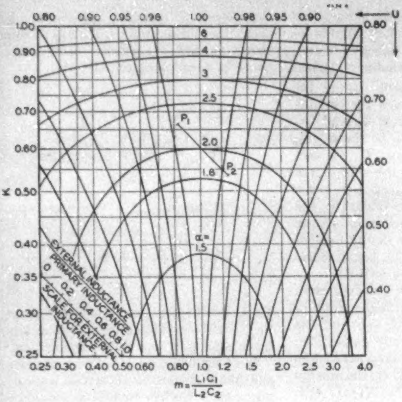

Figure 4. Chart for determination of secondary voltage

The first factor, U, is a measure of the amplitude of the secondary wave and also is at the most equal to 1.

The quantities U and α, determining the amplitude and wave shape of the secondary voltage, are functions of only two parameters, the coupling coefficient K between the circuits, and the quantity $m = L_1C_1/L_2C_2$ equal to the square of the ratio of natural frequencies of the secondary and primary circuits taken separately. These functions are represented on the chart of Figure 4.

By studying the chart, and the curves of Figure 3, the following conclusions may be reached:

1. All the energy initially in the primary capacitor will be transferred to the secondary capacitor if the primary and secondary circuits, when taken separately, have equal natural frequencies, and if the coefficient of coupling between the two circuits is 0.60.
2. These two conditions are not critical, only a slight reduction in peak secondary voltage resulting from a fairly wide departure from the ideal conditions.

The first condition gives maximum amplitude for the secondary voltage, and, combined with the second condition of 0.60 coupling coefficient, makes the higher natural frequency of the system double the lower. Maximum efficiency also could be obtained with frequency ratios of 4, 6, etc., but the corresponding coupling coefficients, 0.882, 0.956, etc., may be higher than can be practically incorporated in this type of transformer.

Effect of Primary-Lead Inductance

A certain amount of external inductance in the primary circuit, contributed by leads, capacitor, and so forth, may be appreciable when compared to the transformer primary inductance, so that its effect cannot be neglected.

If K' is the coupling coefficient of the transformer, L_1' the inductance of its primary winding, L_1'' the external inductance (Figure 5), the circuit constants can be written:

$$L_1 = L_1\left(1+\frac{L_1''}{L_1'}\right) \tag{7}$$

$$K = \frac{K'}{\sqrt{1+\frac{L_1''}{L_1'}}} \tag{8}$$

If, on the chart of Figure 4, P_1 represents the operating point of the transformer without external inductance (K' and $L_1'C_1/L_2C_2$) a simple construction makes it possible to determine the operating point P_2 for the circuit with the external inductance L_1'' in series with the primary inductance. A 45-degree line is drawn through P_1, and a distance scaled along the line corresponding to the ratio L_1''/L_1' of external to primary inductances. The scales for m and K on the chart have been chosen so that the point P_2 thus determined represents operation with external inductance.

It is interesting to notice that a fairly large percentage of external inductance can have negligible effects and that, in some cases, an increase in external inductance actually may raise the secondary voltage.

Effect of Resistance Losses

So far, only an ideal "no-loss" circuit has been considered. Such an analysis will not predict the actual output of an oscillation transformer, but an upper limit that can be approached only by careful design. It gives, however, some very helpful information as to the optimum relationships between the circuit constants, and the decrease in output to be expected when these constants are varied from their optimum values.

Many kinds of losses will cause the actual output voltage to be lower than the values given by a "no-loss" analysis:

1. Corona losses.
2. Dielectric losses.
3. Eddy-current losses in all metallic parts surrounding the high-frequency circuit, such as shielding.
4. Hysteresis losses in magnetic materials.
5. Voltage drop across the device used for discharging the capacitor.
6. Resistance losses in circuits.
7. Losses due to leakage resistance across the terminals.

Only losses due to resistance of the circuits will be briefly studied in this paper, not because all other losses can be neglected, but rather because only resistance losses lend themselves to a fairly simple mathematical analysis. While the effect of losses such as those due to eddy currents and hysteresis can be evaluated roughly by analysis, actual tests afford a simpler and more reliable way to study them.

Equation 1 can be modified by adding terms representing the effect of resistances R_1 and R_2 of the primary and secondary circuits respectively:

$$(L_1L_2-L_{12}^2)p^4+(L_1R_2+L_2R_1)p^3+\left(\frac{L_1}{C_2}+\frac{L_2}{C_1}+R_1R_2\right)p^2+\left(\frac{R_1}{C_2}+\frac{R_2}{C_1}\right)p+\frac{1}{C_1C_2}=0 \tag{9}$$

It should be noted that R_1 and R_2 are a-c resistances, usually higher than the d-c resistances, unless the wire sizes used are small enough in comparison to the depth of penetration to make skin effect negligible.

In a study of general scope such as the present, it is more desirable to express

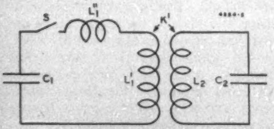

Figure 5. High-frequency ignition circuit with external inductance

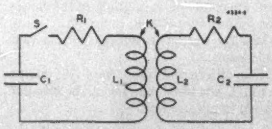

Figure 6. High-frequency ignition circuit including winding resistances

the resistances in "per unit" values than in ohms. The amount of damping in each circuit can be represented by the dimensionless quantities:

$$\rho_1=\frac{R_1}{2\sqrt{\frac{L_1}{C_1}}} \tag{10}$$

$$\rho_2=\frac{R_2}{2\sqrt{\frac{L_2}{C_2}}} \tag{11}$$

ρ_1 and ρ_2 being, for each circuit taken alone, the ratio of actual resistance to the resistance for critical damping.
Equation 9 can now be written:

$$(1-K^2)p^4+2\left(\frac{\rho_1}{\sqrt{L_1C_1}}+\frac{\rho_2}{\sqrt{L_2C_2}}\right)p^3+\left(\frac{1}{L_1C_1}+\frac{1}{L_2C_2}+\frac{4\rho_1\rho_2}{\sqrt{L_1C_1L_2C_2}}\right)p^2+\frac{2}{\sqrt{L_1C_1L_2C_2}}\left(\frac{\rho_1}{\sqrt{L_2C_2}}+\frac{\rho_2}{\sqrt{L_1C_1}}\right)p+\frac{1}{L_1C_1L_2C_2}=0 \tag{12}$$

In all practical cases, the amount of damping will be small, $\rho_1\ll 1$ and $\rho_2\ll 1$. If the phase angles, which are small and would introduce only amplitude errors of the second order in the neighborhood of the peaks are omitted, the voltage output of the transformer can be written:

$$\frac{v_2}{V_2}=U\times\frac{\epsilon^{-\alpha_1 t}\cos 2\pi n_1 t-\epsilon^{-\alpha_2 t}\cos 2\pi n_2 t}{2} \tag{13}$$

U, n_1, and n_2 differing only by second order quantities from the values found previously in the "no-loss" case. The decrement factors, $-\alpha_1$ and $-\alpha_2$, are the real parts of the roots of equation 12.

In order further to simplify the problem, it will now be assumed that $L_1C_1=L_2C_2$, this being a desirable condition for operation ($U=1$). The circuit damping coefficients ρ_1 and ρ_2 now enter into equation 12 only as their sum $\rho_1+\rho_2$, so that α_1 and α_2 will be functions of $\rho_1+\rho_2$. In other words, a certain per unit damping will have exactly the same effect upon the output, whether it is in the primary or the secondary circuit.

If the coupling between circuits has been chosen for optimum wave shape, the maximum voltage will be reached approximately when $\cos 2\pi n_1 t=-1$ and $\cos 2\pi n_2 t=1$, so that v_2/V_2 will have a peak value:

$$\frac{v_{2m}}{V_2}=\frac{\epsilon^{-\alpha_1 t}+\epsilon^{-\alpha_2 t}}{2} \tag{14}$$

If all quantities beyond the first order are neglected:

$$\frac{v_{2m}}{V_2}=1\frac{(\alpha_1+\alpha_2)t}{2} \tag{15}$$

The sum of the roots of equation 12 is $-2(\alpha_1+\alpha_2)$, the imaginary parts canceling out as the roots form two sets of conjugate complex quantities. It is thus possible to evaluate $\alpha_1+\alpha_2$ without solving equation 12, the sum of the roots being equal to the negative value of the ratio of the coefficient of p^3 to the coefficient of p^4.
Thus, from the coefficients of equation 12:

$$\frac{v_{2m}}{V_2}=1-\frac{(\rho_1+\rho_2)t}{2(1-K^2)\sqrt{L_1C_1}} \tag{16}$$

t, is of course the time at which the peak occurs.

The most interesting case to study is the optimum case determined in a previous section, for $m=1$ and $K=0.60$. Without losses, the maximum voltage would occur when $2\pi n_1 t=\pi$. If a small amount of damping is added, the displacement of the peaks due to this damping can be neglected, and the effect of resistance upon peak secondary voltage can be computed by equations 16 and 2, the latter being used to determine the lower natural frequency n_1.

$$\frac{v_{2m}}{V_2}=1-3.11(\rho_1+\rho_2) \tag{17}$$

Conclusions

It is hoped that this study will help elucidate some of the fundamental concepts connected with this type of circuit. In particular, it is interesting to note that there are no critical conditions of resonance in this circuit, such as there would be if it were operated under steady-state rather than transient conditions. Also, the fact that a very high coefficient of coupling is not necessary for satisfactory operation may sound surprising at first.

The great majority of electrical problems deal with steady-state conditions, so that, whenever transient phenomena have to be considered, as in the present case, different methods of analysis and special measuring equipment are required. Fast-recurring transients usually can be studied with the aid of a cathode-ray oscilloscope, providing the extra load on the system does not change materially the transient being studied. In high-frequency ignition circuits, it is often impossible to satisfy this condition, and it may even be difficult to obtain an oscilloscope of sufficient writing speed.

There are, however, certain simple relations between the natural frequencies of oscillation of coupled circuits with low damping and the resonant frequencies of such a circuit excited by a sine-wave oscillator. For instance, if the input impedance of the circuit of Figure 1 is measured at the switch *S* for various frequencies, the curve of impedance versus frequency will show a maximum for a frequency approximately equal to the natural frequency of the secondary circuit taken alone and two minima corresponding to the two component frequencies of the transients studied in previous sections. The relative amplitudes of the maximum and minima give an indication of losses in the circuits. Such measurements can be made with standard equipment: a radio-frequency oscillator, a vacuum-tube voltmeter and a hot-wire ammeter. Even though they have to be taken at low flux densities, they give some interesting data that can be of great help in the test of high-frequency ignition circuits. The development of such test methods and of special measuring equipment should parallel the development of the circuits, to give a better understanding of their operation and thus guide the engineer responsible for their design.

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Inherent Overheating Protection of D-C Aircraft Motors

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Synopsis: Inherent overheating protection is undoubtedly one of the major developments in the fractional-horsepower-motor field in the last decade. Thermal protection is now advocated by some for aircraft motors, and many thermally protected motors are now in aircraft service. In this paper are reported the results of a number of laboratory tests taken on a typical aircraft motor. Tests were taken on a $^{1}/_{15}$-horsepower 7,500-rpm d-c motor; loads ranged from locked rotor to the minimum running load sufficient to trip the thermostat. Ambient temperatures ranged from −58 to +78 degrees centigrade. Locked-rotor and running-overload temperature tests were also taken at a density altitude of 42,000 feet. Temperatures of the winding, frame, brush holders, and bearings were measured carefully under all conditions mentioned. Satisfactory protection, without premature tripping, was invariably achieved.

IN 1925, introduction of thermostatic protection to domestic flatirons revolutionized the flatiron industry. In 1928, disk-type thermostats were built into a portable-tool motor to provide built-in thermal protection. Two or three years later, thermostats were used to provide built-in overload protection for single-phase induction motors. Since that time, a quiet revolution has been taking place in the fractional horsepower motor industry, in the form of an ever-increasing use of these built-in thermal protective devices. By 1939, the Underwriters' Laboratories had become interested, and in June of that year, after conferring with electrical manufacturers making and using such devices, the Underwriters' Laboratories wrote a standard for inherent overheating protective devices; this standard was incorporated as an appendix to the Underwriters' standard for industrial control equipment. If the present trend to thermal protection of motors continues, nearly all domestic fractional-horsepower motors will eventually be provided with inherent overheating protection.

Just because the merit and utility of inherent overheating protection for industrial fractional-horsepower motors have been indubitably established, it does not necessarily follow that similar protection is necessary, or even desirable, for aircraft motors. Requirements of motors for aircraft service differ so materially from the requirements of motors for industrial or domestic service that the worth of thermal protection for aircraft motors has to be examined in a different light. T. B. Holliday, in a recent article,[6] has pointed out a number of reasons why thermal protection of aircraft motors is desirable. Some of the reasons given by him are:

1. To prevent motor burnouts due to temporary or permanent failures of the driven device. If motor burnout is prevented, the motor itself does not have to be replaced, and as the driven device can often be repaired, a new motor—frequently not available in remote places—is not required.

2. To prevent burnouts due to improper setting or functioning of limit switches. Hydraulic systems, he points out, have relief valves which prevent damage to the system in the event of jamming, and why shouldn't electric motors be similarly protected?

3. Even turret motors should use protected motors; in the event of damage to a part of the track, it is better, Colonel Holliday argues, to save the motor, and thereby allow it to be used to traverse the good section of the track, rather than have the motor burn out completely.

4. A nonprotected landing-gear motor may burn out in an attempt to put down landing gear in which the grease has stiffened excessively, whereas a thermally protected motor may, by several starts and stops, get the gear down.

Opponents of thermal protection contend that an aircraft motor should be kept energized "to the last ounce of effort"; the "last shot" may mean the difference between life and death to the crew. It is argued that an overload device may trip at a crucial moment, whereas perhaps another second or two of operation of the motor would have saved the emergency situation.

Whatever may be the merit in these contentions, it is beyond the scope of this paper to attempt to analyze and weigh all the controversial aspects as to whether thermal protection should or should not be used for aircraft motors. Rather, the aim of this paper is to present a picture as to just how thermal protection generally functions. It was felt that such a picture could best be presented by reporting specific test data on a representative aircraft motor equipped with inherent overheating protection.

Motor-Thermostat Combinations Tested

Following are the name-plate specifications for the motor selected for this test:

Outside diameter of frame, inches	$2\,^{5}/_{16}$
Volts	24
Horsepower	$^{1}/_{15}$
Rpm	7,500
Amperes at full load	3.75
Wound	Shunt
Manufacturers frame number	G-234
Open or enclosed	Open

A shunt-wound motor was chosen, because that type is more difficult to protect than a series motor.

Disk-type thermostats were used for all tests. While this device is familiar to many, a brief description of the disk principle is in order. The disk-type thermostat has as its single moving element a bimetallic disk. This disk has a dished shape, with the high-expansive side of the bimetal on the concave side of the disk at normal temperature. As the disk is heated, the bimetal on the inside of the disk tries to expand but is restrained from doing so by the concave shape of the disk, for the other side, being of nonexpansive metal, does not expand similarly; as the temperature of the disk is increased, stresses are set up tending to buckle the disk, and as soon as its

Table I. Locked-Rotor Test on the Enclosed Motor, at 26 Volts, and at an Ambient Temperature of 26 Degrees Centigrade

	Amp Just Before Thermostat Opens	On Time, Sec	Off Time, Sec	Peak Temp, Armature Winding	Temp of Front Bearing
First cycle of thermostat	21.6	4.5	3.5	68	26
Second cycle of thermostat	20.8	1.3	3.6	72	
Third cycle of thermostat	20.6	1.3	4.0	78	
Fourth cycle of thermostat	20.4	1.3	4.6	84	
Fifth cycle of thermostat	20.2	1.4	5.1	90	
Sixth cycle of thermostat	20.0	1.3	5.3	94	34
After 10 min, lapsed time	16.8	1.8	20.5	172	64
After 30 min, lapsed time	16.8	1.8	33.4	168	99
After 64 min, lapsed time	16.7	2.0	34.4	164	100

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The author makes grateful acknowledgment to the Spencer Thermostat Company at Attleboro, Mass., who made available the use of their laboratory, where the majority of the tests reported in this paper were conducted; specifically he thanks the following for their assistance in performing these tests: L. Wood Buell, Jack Duffy, Edith Pitts, and William O'Brien. He further acknowledges valuable assistance from J. D. Miner, manager of engineering, and F. A. Heddleson, special development engineer, both of whom are at the Lima works of the Westinghouse Company.

temperature has reached a certain predetermined value, the disk snaps suddenly over to the other position. When the disk cools to a definite temperature, it will snap back to its original shape. It is to be noted that this disk operates when, and only when, it reaches a certain fixed temperature, called the opening temperature. This temperature may be reached as a result of heat received from the motor, by heat generated in an auxiliary heater within the thermostat, or by both. All of the thermostats used in the tests reported herein contain an auxiliary heater, as well as the disk.

Three motor–thermostat combinations were tested:

1. Totally enclosed with a thermostat mounted on the end of the front bracket, as shown in Figure 1.
2. As a normal open motor, with a "silk-hat" type thermostat mounted on the motor frame, as shown in Figure 2.
3. As an open motor with a thermostat inside the front bracket near the commutator.

The first arrangement was chosen as representing a practical arrangement for a totally enclosed motor. While the normal horsepower rating of an open motor is reduced by enclosing, it was felt that better comparative results would be obtained if the same motor were used for all tests; in reviewing the tests, it should be borne in mind that the motor would normally be rated at less than $^{1}/_{15}$ horsepower when enclosed. A "silk-hat" type thermostat, as shown in Figure 2, is a normal mounting arrangement for an open motor. The third arrangement—a thermostat inside the motor—was selected for these tests because it was felt that the inside of the motor was the most logical location from a thermal standpoint. Therefore, this arrangement was tried experimentally, although it was recognized that it would not be practicable in production motors because of space considerations.

Tests on these three motor–thermostat combinations afford a good comparison of the effect of thermostat location, and illustrate the general characteristics of inherent overheating protection afforded by a disk-type thermostat.

Temperature Measurements

For locked-rotor tests which were taken first, thermocouples were soldered to the armature winding at each end; one thermocouple was tucked into an interstice in the armature end winding in order to compare temperatures with those obtained by the soldered couples. Similarly, thermocouples were also soldered to each of the field coils, and to the commutator end of a brushholder. Additional thermocouples were installed to measure temperatures of each of the bearings and of the frame. For running-load tests, the armature thermocouples had to be removed, leaving only the resistance method for determining armature temperatures after shutdown. Resistance readings were taken by a Kelvin bridge at measured intervals of time after shutdown, and a curve of resistance versus time was plotted and extrapolated back to shutdown time; temperature was computed from this resistance.

A variation of the above method, proposed by L. W. Buell, was to determine resistance while the motor was energized by the voltmeter-ammeter method. Potential-drop leads were soldered to commutator risers about 90 degrees apart; voltage drop across these leads was measured and divided by the armature current, obtaining a fictitious resistance; variation of this fictitious resistance was used as an indication of temperature.

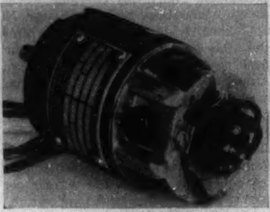


Figure 1. Small aircraft motor with thermostat mounted on the front bracket

A cap (not shown) encloses the thermostat and entire front bracket. Rating, as an open motor, is $^{1}/_{15}$ horsepower, 7,500 rpm

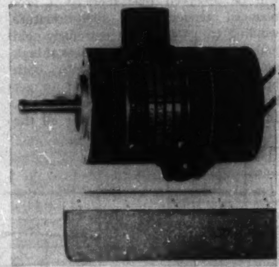


Figure 2. A $^{1}/_{15}$-horsepower 7,500-rpm 24-volt d-c aircraft motor with a "silk-hat" thermostat mounted on the outside of the frame

Some comparisons between temperatures by resistance and by thermocouple are of interest. Soldered thermocouples might be expected to give higher temperatures than unsoldered thermocouples, but on four locked-rotor tests, no significant and consistent difference in observed temperatures was noted. Temperatures by soldered thermocouples were compared with temperatures by resistance; of 13 locked-rotor tests, the thermocouple temperatures ranged from 2 degrees centigrade lower to 19 degrees centigrade higher, and averaged 9.5 degrees centigrade higher than the resistance temperatures. This is equivalent to saying that the temperature rise by thermocouple is approximately six per cent higher than the rise by resistance. A similar comparison was made for 56 temperature tests on fractional-horsepower motors in a previous paper; it was found that the temperature rise by unsoldered thermocouple averaged three per cent more than the temperature rise by resistances.[3]

Types of Tests Taken

Three basic types of tests on thermally protected motors are possible: locked rotor, maximum continuous load, and overload cycling. Locked-rotor tests are important because one of the major reasons for thermal protection is to prevent damage which may arise when the rotor is stalled. Maximum-continuous-load tests are taken to find out just how much load the thermally protected motor can carry without tripping the thermostat. Such a test is taken by loading the motor up to this maximum continuous load until all temperatures have stabilized, and then, increasing the load slightly, causing the thermostat to trip; immediately after tripping, all temperatures are measured. An overload-cycling test is taken by applying a load much heavier than the maximum continuous load, and allowing the motor to cycle on and off at this load value. All three basic types of tests were taken on one of the combinations listed in this paper, but the first two types were taken on all combinations.

A Typical Locked-Rotor Test

Results of one typical test showing how the thermostat cycles on and off and showing how the temperatures and motor current vary during the test are given in Table I.

At first, the thermostat cycles rapidly since the cycling time depends upon only the characteristics of the thermostat itself. In the thermostat used for these tests, the disk opens at a temperature of 147 degrees centigrade, and recloses at a temperature of 116 degrees centigrade. Therefore, during the first on period, the disk heats from 25 to 147 degrees centigrade, requiring 4.5 seconds. During first off period, the disk cools from 147 to 116, then recloses. For the second and each remaining on period, the thermostat disk has to be heated only from 116 to 147; if the motor current were

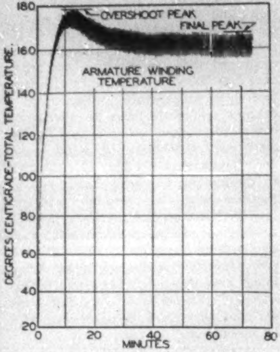


Figure 3. Temperature versus time, locked rotor

constant, this on time would be substantially the same for all remaining cycles, but as the motor current decreases because of the increase in motor temperature, the thermostat on time slowly increases, as can be noted in the table. For each reclosing cycle, the disk cools from 147 to 116, but the length of time required increases slowly as the temperature at the thermostat location increases—in this case, the front-bearing temperature.

Figure 3 shows how the temperature of the armature winding varied during the particular locked-rotor test described. This curve was drawn with a continuous one-point temperature recorder, but in the figure the time axis has been reversed so as to read from left to right. (Ordinarily, on the record of this instrument, time reads from right to left.) The first five or six temperature peaks cannot be seen on this record, since they occur too fast for the instrument to record, but subsequent peaks can be seen. If an envelope were drawn around the peak temperatures reached at the end of each thermostat cycle, this envelope would show an overshoot peak at the end of approximately 11 or 12 minutes; after this, the envelope would approach the final peak temperature asymptotically. Armature winding temperatures given in this report actually refer to the final peaks, that is, to the steady-state value of the envelope of the peak temperatures after stable conditions have been reached. From the standpoint of insulation life, the equivalent temperature actually is even less than the final peak temperature, but for simplicity the final peak temperature has been plotted in all cases.

Summary of Locked-Rotor Tests

Final results for the three motor–thermostat combinations tested are summarized and compared in Figure 4. It will be observed that there is a pronounced tendency for the armature winding to get hotter at extremely cold ambient temperatures than it does at high ambient temperatures. However, both brushholder and bearings tend to get hotter at the high ambient temperatures. In fact, there is a tendency for all the temperatures in the motor to stabilize at the same value at the high ambient temperatures. At an ambient temperature of 147 degrees centigrade (disk-opening temperature), all temperatures would be equal, because the thermostat would not allow the motor to carry any current. Of interest is the fact that similar characteristics are observed for all three motor–thermostat combinations, even including the internally mounted thermostat.

Low line voltage can often occur, and if it does, stalled-rotor conditions are more likely to occur. To determine how this condition would affect the thermal protection, a locked-rotor test was taken on the enclosed motor at 17.3 volts. Temperatures attained during this test are compared with the results of a similar test at 26 volts in the following table:

	Line Volts 26.5	Line Volts 17.3
Armature winding temperature:		
Overshoot peak	178	186
Final peak	176	172
Field temperature	106	109
Bearings (front)	104	102
Brushholder	105	106

No significant difference is apparent, so it is concluded that locked-rotor protection is afforded at reduced voltage, as well as at rated voltage.

It will be observed by comparing the temperatures reached with the temperature-life curves given in Figure 7 that the life of both windings and bearings under locked-rotor conditions is in excess of

Figure 4. Locked-rotor tests

- ⊙ Enclosed motor, thermostat on bracket
- × Open motor, "silk-hat" thermostat
- + Open motor, internal thermostat

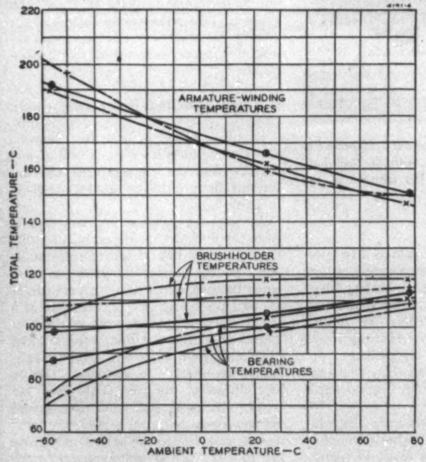


Figure 5. Maximum-continuous-load tests

- ⊙ Enclosed motor, thermostat on bracket
- × Open motor, "silk-hat" thermostat
- + Open motor, internal thermostat

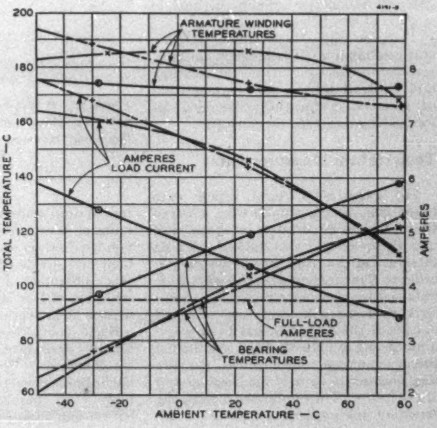
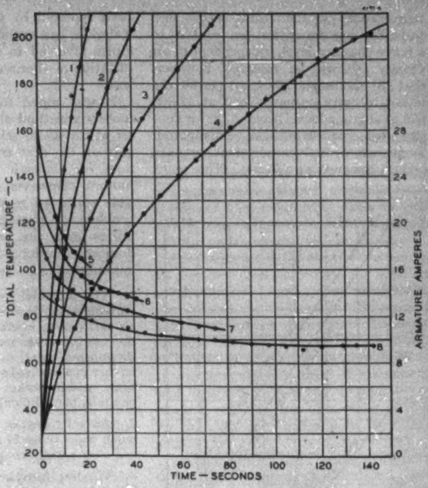


Figure 6. Locked-rotor heating curves

Frame G234, 1/15-horsepower CDH aircraft motor, 24 volts, 3.75 amperes, 7,500 rpm, shunt wound, enclosed



1—Armature-winding temperature by thermocouple, at 29.9 volts

2—Armature-winding temperature by thermocouple, at 24.0 volts

3—Armature-winding temperature by thermocouple, at 19.5–19.3 volts

4—Armature-winding temperature by thermocouple, at 15.3 volts

5—Armature amperes at 29.9 volts

6—Armature amperes at 24.0 volts

7—Armature amperes at 19.5–19.3 volts

8—Armature amperes at 15.3 volts

the Army's requirement of a minimum of five hours. (The corresponding life required by Underwriters' Laboratories for industrial applications is 15 days.) However, the temperatures are far in excess of those considered "safe" for industrial or domestic practice. They have been made higher deliberately, in order not to interfere with the output of the motor and because it is much more economical to operate aircraft electric equipment at high loads in spite of shortened life.

Locked-rotor heating curves for this motor were taken at four different voltages and these are plotted in Figure 6. In taking these curves the thermostat was left out of the circuit and both temperature and armature current were measured at frequent intervals. Of significant interest is the current–time curve, because torque falls off at the same rate as the current (in a series motor, it falls off faster than the current). If the load is so great the motor cannot start it cold, no useful purpose is accomplished leaving the motor on the line, for the torque steadily decreases. An unprotected motor burns out in a matter of seconds; a protected motor keeps trying intermittently for hours. If the load moves only a little with each cycle of the thermostat, the protected motor will eventually do its work while the nonprotected motor burns out and is completely useless; it is much worse than useless if it starts a fire.

Maximum-Continuous-Load and Overload-Cycling Tests

Maximum continuous load was defined earlier in the paper as the maximum load the motor can carry without tripping the thermostat. Experience has shown, both for industrial-type and for aircraft motors, that there is a general tendency to obtain higher winding temperatures if the motor is operated at the least possible load which will cause tripping. If the motor is operated at a load greater than the maximum continuous load, the thermostat trips in a relatively short time, but the motor temperatures are generally lower than they are at the end of a continuous run at maximum continuous load. However, if the load is maintained and if the thermostat is allowed to cycle, the final peak temperatures may be slightly higher than on a maximum-continuous-load test. Overload-cycling tests were taken only on the enclosed-motor combination, and it was found that, at or below room temperature, the armature-winding temperatures were substantially the same as for those of the maximum-continuous-load test; at higher ambient temperatures, however, there was a definite tendency to obtain lower temperatures on a cycling-overload than on a maximum-continuous-load test.

A summary of the results of maximum-continuous-load tests for all three motor-thermostat combinations is given in Figure 5. How much load the motor can carry without tripping the thermostat at various ambient temperatures is useful information, both to the motor designer and to the motor user. This is shown by the "amperes load current" curve. It is of interest that the thermostat allows the open motor to carry, even when enclosed, full-load rating up to +60 degrees centigrade. However, as an open motor, full-load rating can be carried at ambient temperatures higher than 80 degrees centigrade. It is noteworthy that the final temperatures reached by the armature winding are more or less independent of the ambient temperature, although there appears to be a slight tendency for the thermostat to cut off at lower winding temperatures when the ambient temperature is high. This trend is neither so definite nor so positive as it is in the case of locked-rotor tests. Contrariwise, it will be observed that the bearings tend to get hotter at high ambients, as might be expected; also the bearings get hotter in the enclosed motor than they do in the open motors. Limiting factor in this case is the bearing temperature of 140 degrees centigrade, which indicates a life of less than one day.

Altitude-Chamber Tests

Tests were taken in the high-altitude laboratory at Lima, Ohio, to determine what effect altitude might have upon the operation of the thermostat.[8] For tests at altitude, the pressure in the chamber was reduced, but the temperature was not, primarily for three reasons: to determine the effect of pressure alone, as the effect of ambient temperature had already been studied at ground level, some aircraft motors are operated at high ambient temperatures at altitude, and because the testing procedure was simpler and quicker. A locked-rotor test was taken, at ground level, and repeated at altitude conditions, using the same instruments, the same technique, the same personnel, and the same procedure. Similarly, a maximum-continuous-load test was taken, first at ground level, then at altitude conditions. Results of these four tests are given in Table II.

Performance of the thermostat at altitude was almost identical with operation at ground level; there was no significant difference in temperature reached, nor did the thermostat have any difficulty interrupting the locked-rotor current of 12 to 14 amperes at a density altitude of 42,000 feet.

Most interesting is the comparison of the maximum-continuous-load test at ground level with that at altitude. Armature-winding temperatures permitted by the thermostat are practically identical, and since this is the most critical temperature, the thermostat is thus seen to be limiting the critical temperature to a fairly constant level, independent of air density. It will be observed, however, that the thermostat does not permit the motor to carry so much load at 42,000 feet as at ground level because the air density is less, and less heat can be dissipated. Temperatures of the mechanical parts are higher, however, at altitude. This point is not difficult to understand, when it is realized that, since the total losses dissipated are less, less difference between hot-spot and frame temperatures would be expected; if hot-spot temperatures are held the same, the frame temperature will be higher with less effective ventilation. This same effect was noted for the enclosed motor in Figure 5.

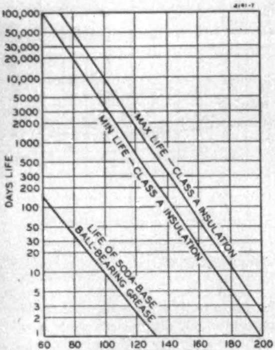


Figure 7. Effect of temperature on life of insulation and grease

Insulation life from Miner, "Insulation of Electrical Apparatus," figure 301, and grease-life information from ball-bearing manufacturers

Selection and Application of Thermostats

A general theory of thermal overload protection was published previously by the author.[2,3] This theory related primarily to single-phase induction motors, and showed how it was possible by means of a properly designed heater in the thermostat to make the temperature of the disk follow the temperature of the windings fairly closely. This theory was further extended quantitatively in unpublished notes to the development of mathematical formulas for selecting the right thermostat to protect any given motor properly at all loads and at all ambient temperatures. Exhaustive tests showing how effective inherent overheating protection can be in this type of motor have been reported by C. P. Potter.[4] V. G. Vaughan has outlined some of the principles involved in the selection and application of thermostats for aircraft motors.[5]

It was hoped to develop for aircraft motors a method of mathematical analysis which would select the right thermostat on the first trial the same as was done for domestic fractional-horsepower motors. However, it has not been possible so far to realize this hope for the problem of protecting aircraft motors differs from the problem of protecting domestic fractional-horsepower motors in a number of respects:

1. The principal source of heat, which is variable, is in the rotating winding of the d-c aircraft motor, whereas it is in the stator of the a-c induction motor. This means the thermostat must necessarily be removed farther in a thermal sense from the source of heat, which makes the problem fundamentally more difficult.

2. Aircraft motors must operate over a much wider range of ambient temperatures, and over a wider range of altitudes.

3. Windings must be allowed to reach much higher temperatures in order to prevent nuisance trip-outs which are many times more serious in aircraft than in domestic service.

4. Under locked-rotor conditions, the ratio of final-peak temperature to temperature at the end of the first thermostat cycle, appears to be much higher in d-c aircraft motors, than in industrial single-phase induction motors. Thus the first time of opening with locked rotor, is of less significance.

It appears that the only really satisfactory method is to determine the correct thermostat by actually running tests on a number of trial thermostats. Experience, however, is of invaluable assistance in developing simple rules-of-thumb which reduce the number of thermostats to be tried. Sometimes satisfactory results can best be obtained by making modifications in location or method of mounting the thermostat. As a specific example, when tests were first taken on the motor with the internal thermostat, it was found that considerable ventilating air was reaching the disk of the thermostat. This air tended to cool the disk, thereby permitting the motor to operate at higher temperatures on running loads; but it had no effect on locked-rotor temperatures. This difficulty was surmounted by putting a cap over the thermostat so that ventilating air could not strike the disk directly. Thus, the ratio of locked-rotor temperature to running temperature was increased without changing the thermostat itself. It is anticipated that, at other times, means may have to be taken to change this ratio of temperatures in the opposite direction. Success in applying thermostats to aircraft motors might be said to follow Edison's formula for successful invention: 2 per cent inspiration, 98 per cent perspiration.

Table II. Thermal-Overload Tests in a High-Altitude Chamber

	Ground Level	Altitude
Locked-rotor tests		
Air temp, C	+30	+33
Pressure altitude, ft		35,000
Density altitude, ft		42,000
Temp at end of test		
Armature winding by thermocouple	176.5	180.5
Shunt field by thermocouple	108.5	109.5
Brushholder by thermocouple	118	118
Bearings (front) by thermocouple	107	108
Frame by thermocouple	100.5	100.5
Thermostat case	90	93
Maximum-continuous-load tests		
Air temp, C	35	33
Pressure altitude, ft		35,000
Density altitude, ft		42,000
Maximum continuous load, amp	5.75	4.3
Temp at end of test		
Armature winding by resistance	180	186
Shunt field by thermocouple	95	126
Brushholder by thermocouple	103	130
Bearings by thermocouple	99.5	123
Frame by thermocouple	84	113
Thermostat case	80	105

Summary and Conclusions

Possibilities of inherent overheating protection for aircraft motors were investigated by taking comprehensive tests on a small aircraft motor under a wide variety of controlled conditions, including variations in: line voltage, load, ambient temperature, enclosure, thermostat location, and altitude.

Locked-rotor tests taken at ground level show that effective protection is obtained over the tested range of ambient temperatures from −58 to +78 degrees centigrade. Slightly better protection is afforded the armature winding at the upper end of this temperature range, but the bearings are better protected at the lower range, as can be seen by reference to Figure 4. A reduced-voltage test demonstrated that the protection is as effective at 17 volts as at 26 volts. Altitude-chamber tests demonstrated that the effectiveness of the protection is not impaired at 40,000 feet, also that the protective device has ample interrupting capacity at this altitude.

Running-load tests showed that effective protection is obtained over the tested range of ambient temperatures from −50 to +80 degrees centigrade. The general tendency is for the thermostat to cut the motor off the line at nearly the same armature-winding temperature, regardless of ambient temperature, although there is a tendency to limit the armature-winding temperature to a slightly lower value at the upper end of the range. However, temperatures of the bearings are invariably higher at the higher ambients, as can be seen by reference to Figure 5. When the thermally protected motor was taken from sea level

Historical Development of Electric Connectors

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Synopsis: This historical development of connectors endeavors to survey briefly the various stages of electric-connector design and the expanding applications of the uses from the earliest types for heavy-duty power applications to the smaller and more intricate fittings in aircraft, electronics, sound equipment, television, and marine applications. Because of the broad scope of the subject, the material is treated in generalizations in order to cover the history within the space limitations of this paper.

It is our belief that connector design, materials, and manufacture are keeping pace with the requirements of new electric equipment. Since the connector is essentially a component part of electric equipment, its development is affected by the requirements of new electric equipment and through adaptations for the improvement of existing equipment is increasing operating efficiencies.

THE primary role of the multicontact electric-cable connector is to disconnect and break electric circuits rapidly. It is also used as a means of switching circuits.

For wiring and servicing operations, the connector simplifies such procedure, compared, for instance, to the slow disconnection details of the screw-type terminal block. With the connector, the danger of rewiring incorrect circuits is also eliminated. The speed with which circuits can be disconnected or connected in emergencies points to another advantage as a safety factor.

In one instance, operating efficiencies of electric connectors were demonstrated in the nine hours per engine time saved in servicing and overhauling a large transport airplane.

Inasmuch as the term "plug" is often used indiscriminately to describe both receptacle and plug, it is well to define the complete connector and its two main parts. The complete connector comprises both receptacle and plug. The receptacle is that part of a complete connector which is normally "fixed," that is, rigidly attached to or an integral part of a supporting surface. The plug is that part of the complete connector which is normally removable after disengagement. Either plug or receptacle may be provided with socket or pin-contact insert assemblies. The type *AN*3101, however, is an exception to the rule.

The choice of a type letter has, for the most part, been variously adopted, depending upon the general use or even—as in the case of type *F* (Figure 1)—for the company, Fox Motion Picture Studios, for whom it was designed. Fortunately, when the Army and Navy specifications connectors were created, a standardized system of nomenclature of parts as well as types was adopted, for which the engineering profession can well be proud.

The precursor to the electric connectors as modified and improved for aircraft applications is found among those early connectors built by James H. Cannon and associates shortly after 1920. The first of these is termed the *M*1–4. It is still in use today and far from obsolescence. It was designated as type *M*, because it was designed for electric motors, 1 because it was the first of a series, and 4 for the number of contacts in the insert assembly.

Compared with the average connectors in use today in aircraft applications, Cannon type *M* (Figure 2) is oversize. Its shell, however, like aircraft connectors today, is aluminum. The inserts were fabricated of laminated phenolics, one having floating female or socket contacts with tapered bores and the mating insert having fixed split male or pin contacts which assumed the angle of the tapered bore sockets upon insertion. Even this pioneer design did not differ greatly from the aircraft connector of today.

Coupling was maintained only by the wedging action of pin and socket con-

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to 40,000 feet, the following characteristics were observed: less load-carrying ability, substantially the same armature-winding temperature, and higher temperatures of bearings. Enclosing an open motor has a similar effect.

The effectiveness of thermal protection is shown by the fact that, over the entire range of conditions tested, including the three motor-thermostat combinations, the armature-winding temperature was held between the limits of 147 and 196 degrees centigrade. Exclusive of locked-rotor tests, the armature-winding temperature was held by the thermostat, under all conditions, between the narrower limits of 166 to 186 degrees centigrade. These figures are well above the range of temperatures which are encountered in any properly applied aircraft motor under any normal conditions. Hence the thermostat will trip only when abnormal conditions are encountered. But the temperatures permitted by the thermostat are low enough to permit a life of a day or more under abnormal conditions.

In the author's opinion, the temperatures for which the thermostats were set are a reasonable compromise. Appreciably lower values might result in nuisance trip-outs, as unnecessary as they might be dangerous. Use of higher temperatures might possibly be justified on the grounds that a minimum of one day's life is unnecessary, but it should be pointed out that maximum horsepower output of a motor decreases with the temperature. Furthermore, some allowance must be made for variations in individual motors and individual thermostats. Also, there are other parts of the motor which can be damaged by excessive temperature, including: bearings, brushholders, shunts, and so forth.

Results comparable to those reported in this paper are to be expected only if skill and care are used in the application of the thermostat. But when this is done, inherent overheating protection is afforded against all abnormal operating conditions, without danger of preventing the motor from performing its normal functions. Thus motor burnouts and consequent replacement can be prevented by thermal protection.

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tacts; yet this proved satisfactory for the applications. This is in contrast to later developments which introduced types with latch-locking devices providing a quick and positive means of coupling, threaded coupling nuts, and also coupling nuts augmented with provisions for safety wire, which presented a more satisfactory means of overcoming problems of weatherproofing and vibration.

In this early type-*M* connector, floating sockets were used. This method of mounting the socket contacts has remained a fundamental standard.

When sound motion pictures went into production in the late '20's type-*M* connectors were used for certain electric circuits, including synchronous camera motors, microphone circuits, and camera blimps, although later type-*M* connectors were exchanged for smaller and lighter fittings in many cases.

Following the type *M*1, came the *M*2, with a three-inch insert diameter having a greater number of contacts. Next came the *M*3 (Figure 2) with an insert diameter of $4^9/_{16}$ inches. Type M1 carried contacts for 30-ampere service, *M*2 for 30- and 60-ampere service, and *M*3 from 30- to 90-ampere service.

As can be readily seen, the trend to larger connectors carrying a greater number of circuits in the same general design reached a point of diminishing utility. For example, the *M*3-26 (Figure 3) plug weighed $3^1/_2$ pounds. Compare this to an *AN* plug with 30 similar contacts weighing 0.336 pound. Variations of the *M* fittings were made, having up to 30 circuits.

However, it was also apparent that the smaller connector would have a wider variety of uses. This opportunity came in 1925, when a certain connector failed to meet a motion-picture sound studio's requirements both in socket design and delivery. An emergency had arisen. However, within a few days, samples were submitted embodying a newly designed socket machined from one-quarter-inch-square brass rod, having a leaf-type contact spring riveted to one side. Cable clamps were cast onto the brass end bell. Pins were pressed into laminated phenolic inserts. Sockets used were the standard full-floating type, which also enabled the sound engineers to avoid variations in contact resistance when the fittings were moved during recording.

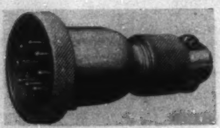
Figure 1. Type-*F* plug, straight-type, with pin insert assembly and cable clamp

Thus the type-*F* Cannon connector was conceived and born, with *F* for Fox Motion Picture Studios as a constant reminder of its motive origin. The *F* type proved successful and soon became the standard connector, not only for cinema sound-production equipment, but in many radio stations.

Actually, it was this type *F* that caught the interest of aircraft and airline engineers, beginning the aircraft phase of connectors. But, in the meantime, to be chronologically correct, the type *P* (Figure 3)—*P* for Paramount Studios—was designed, representing the trend toward smaller and more compact connectors. Type *P* had an insert diameter of one inch, a latch-coupling device, a die-cast shell, and molded inserts. These represented definite advances in design and materials which were reflected in connectors built a number of years later. Type *O*—O for oval design—soon followed type *P* and was designed for the inductor-type microphone.

All during these years the aircraft industry was growing, if slowly, and, with the increasing circuits and new service problems, connectors were soon or later bound to be used. The opportunity came one day late in 1932, when James H. Cannon showed a type-*F* connector to Warren Boughton, chief electrical engineer at Douglas Aircraft Company. At this time the *DC*-1 transport ship was in the blueprint stage. Mr. Boughton believed the type-*F* connector to be adaptable to aircraft requirements. Modifications subsequently were made and accepted. Transcontinental and Western Airlines specified it for use on their new airliners to be built by the Douglas Company.

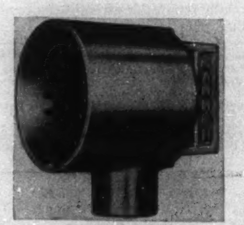
Figure 2. Type-*M*3 90-degree-angle plug, with pin insert assembly

The shell material of the *F* was changed from brass to aluminum, and type *F* became *AF* (Figure 4), *A* for aircraft, and the transition from sound equipment to aircraft was complete.

For use on the type *DC*-2 transport airplane in 1933, the cable clamp entrance on the *AF* connector was eliminated, and threads were put on the cable entrance of the shells to permit the use of flexible conduit. At the same time, requirements for additional contacts made it necessary to increase the number of insert arrangements.

The use of connectors increased the safety of aircraft electric circuits and cut down servicing time by many hours, and maintenance of electric circuits was greatly simplified, resulting in labor and time-saving factors. Although numerous developments have been made since that time, these three basic features of electric connectors seldom have varied and remain essentially the same today.

Following closely upon the *AF*, type *FM* (Figure 5) was developed, having a two-inch insert diameter to meet new requirements of aircraft and the concomitant need for more contacts and greater clearance between contacts. Otherwise, the *FM* was identical to the type *F*.

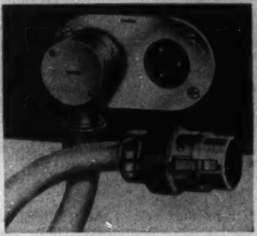
Figure 3. Type-*P* straight cord plug, with socket insert assembly, lower right. Type-*P* 90-degree-angle plug shown upper left engaged in type-*P* two-gang panel receptacle, flush-mounting, with pin insert assemblies

The continued demand for lighter and smaller connectors used in many applications such as aircraft fire-wall connections, radio transmitters, radio control panels, tachometers, resistance thermometers, gauge indicators, remote-control thermostats, pumps, various small motors, lights, and governors, resulted in the design of the type *K*, (Figure 6), the first connector series to be designed specifically for airborne equipment. The *K* series was built in eight basic shell sizes, with inserts containing from one to 100 contacts, and in a wide variety of contact sizes, voltage spacings, and amperage ratings. Type *K* represented a marked achievement in the design of electric connectors.

In the type *K*, socket contacts were made from round rod stock, as this type of construction provided a means of grouping more contacts within a limited insert area. Thus, mechanical spacing was reduced with no loss in creepage between contacts. The insulators were fabricated out of sheet phenolic, although during the later development of type *K* molded insulators were used in conjunction with the fabricated type. Shell parts were made of aluminum alloy. Coupling was maintained with a coupling nut having a modified Acme thread, which provided a rapid means of coupling. An exception was the large *AK* size (Figure 6) which used the conventional *NF* thread. On the *AK*, the screw-jack action of the coupling nut was necessary to engage and disengage the fitting, because of the force required to engage and disengage fittings having a large number of contacts.

The smallest type *K* has an insert diameter of five-eighths inch, and the largest, $2^1/_4$ inches. Type-*K* shell styles were available in straight, 90-degree-angle, and wall-mounting types, including a special end bell having an integral clamp if required, and accessory fittings included dust caps, dummy, or stowage receptacles, and 90-degree-angle and straight junction shells. Cable entries in the *K* are of two types, taper and straight, with three available conduit threads in several sizes: *B* for Breeze-type thread, once standard for the United States Navy, *AC* for Air Corps and the new *AN* standard.

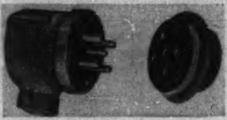

Figure 4. Type-*AF* 90-degree-angle plug, with pin insert assembly, and opposite, type-*AF* wall-mounting receptacle, with socket insert assembly

In 1936, a move to merge the divergent specifications of the Army Air Forces and the Navy Bureau of Aeronautics into one standard was begun for the sake of efficiency and economy. This new Army–Navy specifications on electric connectors was known as *AN*-9534. Since that time, a second specification, *AN-W-C*-591, has been drawn up, with an amended specification pending. The original *AN*-9534, issued November 1, 1939, covered 18 basic shell sizes in five different types: *AN*3100 (wall-mounting), *AN*3102 (box-mounting), *AN*3106 (straight), *AN*3108 (90-degree-angle), and type *RC* (integral-mounting). At that time 53 insert arrangements were available, whereas the total now exceeds 250.

For the type *AN*3102, see Figure 7 and Figure 8; type *AN*3106, Figure 9 and Figure 10; type *AN*3108, Figure 11.

It should be noted that type-*RC* integral mounting was intended for use where the shell of the receptacle is made an integral part of the electric equipment housing.

AN-9534 SPECIFICATION:*

A. Plug and receptacle, with the exception of receptacle type *RC*, were to be interchangeable between their respective plug and receptacle shells, which applied to each manufacturer's own product and did not require the inserts made by one manufacturer to fit any other manufacturer's plug or receptacle shells.

B. Contacts were made from material of high-grade conductivity. Contacts silver-plated, except those made of thermocouple material.

C. Interior face of solder cups completely tinned.

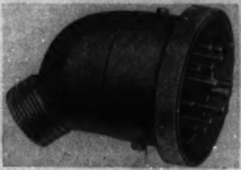

Figure 5. Type-*FM* 45-degree-angle plug, with pin insert assembly

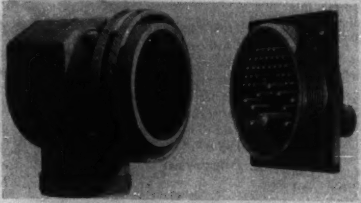

Figure 6. Type *K* (*AK* shell size shown with *NF* thread on coupling nut)

Left, 90-degree-angle plug with socket insert assembly. Right, wall-mounting receptacle with pin insert assembly

D. Pin and socket contacts sizes 0, 4, and 8 were designed so that they could be readily removed from their inserts for soldering to conductors and easily assembled after the soldered connection was made.

E. Potential millivolt drop not to exceed 7 to 12 on contact sizes 20 to 0, with a range of current from 5 to 200 amperes, between each contact and other contacts and shell.

F. Separating force ranged from two pounds on size—20 contact to 20 pounds on number—0 contact.

G. Four service ratings—instrument (peak voltage of 70); 24 volts (peak, 100); 110 volts (peak, 350); and 500 volts (peak, 1,000).

H. Allowable creepage was based on service for which insert was designed; namely, $^1/_{16}$ inch on instrument, $^1/_8$ inch on 24 volts, $^3/_{16}$ inch on 110 volts, and $^1/_4$ inch on 500 volts.

I. Three positioning inserts made possible three distinct polarizations, numbers 1, 2, and 3, by rotating insert in shell. Thus, three identical inserts placed side by side would have distinctly different polarization.

J. Plugs were of such construction that it would be possible to install a plug on both ends of a conduit assembly without excessive slack in the conductors within the conduit. To meet these requirements, Cannon designed an end bell which was split in half so that both halves could be removed to provide ample space for servicing.

K. Shells and receptacle fittings were made of high-grade aluminum alloys.

L. Phenolic materials were used in insert assemblies.

The amended *AN*9534a specification, issued in December 1941, superseding *AN*9534, contained the following main changes:

A. Requirements for three positions of inserts were removed, and only position 1 was retained.

B. Original specifications provided for the maximum operating voltages for direct current and alternating current under the various service designations. Under the amended specification *AN*9534*a*, the a-c maximum was removed and the d-c table retained. However, reference to alternating current or direct current was removed.

C. The number of insert arrangements was increased to 87.

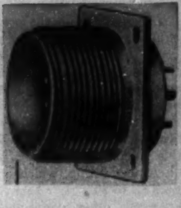

Figure 7. Type-*AN* 3102 receptacle with pin insert assembly

D. Type-*RC* receptacle was assigned drawing number *AND*10066.

The new specification *AN-W-C*-591, issued in December 1941, which became effective June 1942, carried the bulk of *AN*9534a as well as *AN*9534, with the following changes:

A. Deletion of markings of instrument, 24 volt, 110 volt, and 500 volt, from the face of the insulator. Instrument, 24 volt, and 110 volt were grouped under service *A*, 500 volt under service *B*, and such high voltages as 14,000 under service *C*.

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* Adapted from the Army–Navy specifications.

B. Since military aircraft, in particular, was seeking higher altitudes, effective creepage distances were increased. Service *A* with a peak voltage of 200 required one-eighth-inch minimum creepage distance; service *B* with a peak voltage of 750, $^5/_{16}$ inch; and service *C*, 14,000 volts, one inch. To maintain the interchangeability of inserts and also retain the mechanical spacing, but increase the effective creepage distance, it was necessary to design barriers on the face of the inserts.

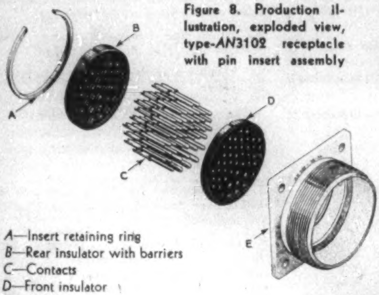

Figure 8. Production illustration, exploded view, type-*AN*3102 receptacle with pin insert assembly

A—Insert retaining ring
B—Rear insulator with barriers
C—Contacts
D—Front insulator
E—Shell

C. Shell sizes 44 and 48 were eliminated from the new specification.

D. Type-*RC* integral mounting was issued a number series for shell sizes, *AND*10425 to *AND*10436.

The newest *AN* connector is type *AN*3101 which, like the *AN*3106 and *AN*3108, embodies the portable features of a plug, but is not a fixed mounting like types *AN*3102 or *AN*3100. However, type *AN*3101 has the threaded cable entry of a receptacle. This new type is being made in sizes 8*S* to 40 inclusive, under the proposed *AN-W-C*-591*a* specification. Another new *AN* type under this specification is the *AN*3017, quick-disconnect fitting, which is restricted to sizes 8*S* to 20.

Among other connectors which are used in one way or another on aircraft or other war material which are of considerable importance but rather specialized in application, are the type *DP* and battery connectors, both developed within the last four or five years.

The *DP* is best generally described as rectangular in shape. *DP*'s are particularly adapted to rack-mounted radio and range-finding equipment. Several *DP* types mount directly to the outside face of the chassis itself, alignment being effected by a taper in the shell and by the contact arrangement itself. Coaxial contacts for radio antennas are standard in several of the *DP* fittings. Contacts vary from 10 to 135, the largest connector in the series being type *DPL* used for special equipment by the Army Air Corps.

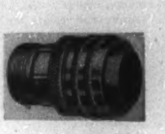

Figure 9. Type-*AN*-3106 plug, with socket insert assembly

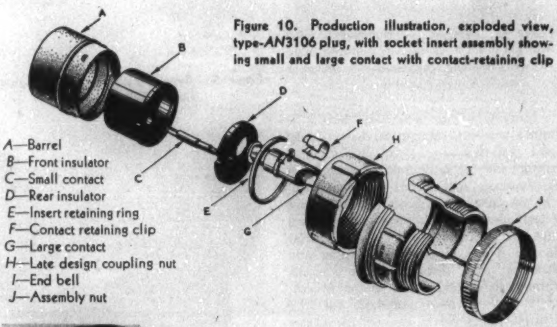

Figure 10. Production illustration, exploded view, type-*AN*3106 plug, with socket insert assembly showing small and large contact with contact-retaining clip

A—Barrel
B—Front insulator
C—Small contact
D—Rear insulator
E—Insert retaining ring
F—Contact retaining clip
G—Large contact
H—Late design coupling nut
I—End bell
J—Assembly nut

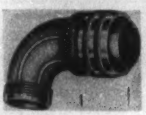

Figure 11. Type-*AN*3108 90-degree-angle plug with socket insert assembly

A notable development of the line of battery connectors, which have been in use for a number of years in engine starting and other applications, is the type numbered 11,749 and 11,751, which was developed at the request of the Air Corps to facilitate disconnection of batteries, particularly in extremely cold climates. The design formerly in use required the maintenance mechanic to loosen two small wing nuts before removing the battery, a slow and difficult process in subzero weather. During removal, the battery contacts were likely to strike the aluminum side of the air frame, creating a short circuit and consequent fire hazard, whereas this new development completely encloses the contacts. In addition, it has a handwheel for disconnection, which can easily be turned with a gloved hand.

The materials in the insert insulators constantly are being improved to overcome high-altitude problems, humidity, and fungus growths of tropical climates, and circumstances of salt-water conditions. Research in thermoplastics, thermosetting materials, and protective coatings is going forward rapidly.

Briefly, among the other developments which are yet to be recorded in the chapters on the history of electric connectors are snap-in solderless contacts. Still other new developments not entirely out of the experimental stages include the following: connectors having a greater voltage for high cycle; special switching devices which operate engine-starting units; and waterproof designs for marine and ordnance requirements.

The Testing of Brushes for Life and Performance Under Various Altitude Conditions

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Synopsis: This paper presents the author's experience in the testing of brushes on machines intended for high-altitude service on aircraft equipment. The procedure and range of conditions found useful in testing a wide variety of small a-c and d-c machines are outlined in detail. It is hoped that other investigators will contribute their experiences through discussions or additional papers, and that from this material there may be developed a suitable test code, supplementing the present Institute test code for d-c machines.

The only real criterion of brush performance is that of actual experience in service, since all of the infinite variety of conditions that affect brush performance cannot be anticipated under test conditions. However, carefully conducted tests over a range of specified conditions do provide a good basis for brush selection and greatly expedite the final choice for the best over-all performance. For this reason, and because it will be helpful to investigators in comparing their results, it is hoped that the proposed brush test code may be undertaken in the near future.

IN brush-life testing on machines, many confusing, conflicting, and variable results are observed even under normal atmospheric conditions. These uncertainties are increased greatly in high-altitude tests. Many tests by various laboratories have shown that most brushes are affected greatly by the changes in humidity, oxygen content, or pressure of the surrounding air. It is a common experience for carbon brushes to wear away at extreme rates up to half an inch or more per hour when run on high-speed commutators or slip rings in extremely dry atmospheres.

This rapid wear can be inhibited by a great variety of contaminants if present in small quantities in the air, in the brush, or even in the metal of the commutator. Test results therefore are likely to vary greatly unless scrupulous care is taken to exclude unwanted materials and to maintain precise control of the atmosphere.

It is not the purpose of this paper to discuss any of the methods for preventing brush wear, but simply to describe the testing procedure found useful in obtaining definite and reasonably consistent results in the measuring of brush performance. Uniform methods of conducting and reporting brush-life tests are proposed, so that wherever or by whomever tests are made, the results will be of value for record and will be truly comparable with other tests made at other times and places.

General Considerations

The primary purpose in brush testing is to determine, as nearly as possible, if satisfactory operation will be obtained in actual service. Therefore, all known conditions of loading, atmosphere vibration, and power-supply characteristics which occur in service should be duplicated as closely as is practical within the limitations of testing equipment. On many aircraft applications the ranges of load and atmospheric conditions are so extreme and duty cycles are so variable, that the establishment of adequate test cycles may require very careful study and judgment. In many cases it may be impossible to establish satisfactorily brush performance until the representative loads encountered in service are known.

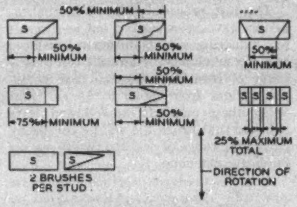

Figure 1. Views of brush face

S represents seated portion of brush

Nearly all of the electrical and many of the mechanical defects which commonly occur on machines adversely affect the life and performance of the brushes. It is impossible to evaluate the brushes unless adequate measurements are taken to make sure that electrical and mechanical conditions of the machine remain substantially constant throughout the test. In order to differentiate between brush failures due to properties of the brush material and the failures induced by mechanical and electrical defects occurring in particular machines, it is necessary to make very comprehensive measurements and observations of all factors known to affect brushes.

Tests should be taken only on machines in good working condition, because the effects of many electrical and mechanical defects upon brush operation seldom can be appraised accurately. Small machines often are damaged by rough handling causing brinelled ball bearings, bent shafts, damaged or dirty commutator or collector surfaces, bad balance, bent or improperly located brush holders; all are factors which adversely affect the brushes. Where there is any possibility that careless handling has occurred, very careful mechanical inspection should be made before any tests are begun.

To predict brush life reasonably well on the basis of relatively short tests, careful study of the nature of commutator, ring, and brush wear is necessary. It appears necessary also to study the films formed on commutators and rings for evidence of any marked changes in appearance with time. The gradual formation of excessive or uneven film during several days of operation has often appeared to be the principal cause of brush failure.

The "Brush-Qualification Test Procedures" outlined in the appendix have as their purpose the determination of three fundamental factors of brush operation which are often almost equally important. These factors are: brush life, commutator life, constancy and suita-

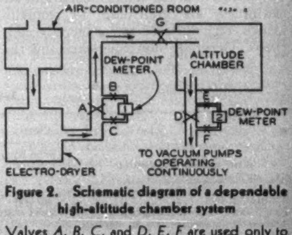

Figure 2. Schematic diagram of a dependable high-altitude chamber system

Valves *A*, *B*, *C*, and *D*, *E*, *F* are used only to produce sufficient pressure head across the dew-point meters to produce distinct meter readings. Meter 1 reads dew point of the air entering the altitude chamber. Meter 2 reads dew point of the air usually in center of altitude chamber. Throttling valve *G* is adjusted to maintain the required altitude

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The author acknowledges the assistance of E. R. Summers, General Electric Company, for preparation of uniform methods for reporting brush test results; J. F. Settle, General Electric Company, and S. W. Glass, National Carbon Company, for their contributions in the development of high-altitude test procedures.

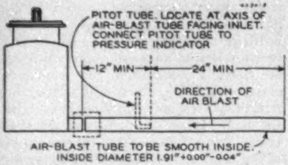
PITOT TUBE. LOCATE AT AXIS OF AIR-BLAST TUBE FACING INLET. CONNECT PITOT TUBE TO PRESSURE INDICATOR
12" MIN
24" MIN
DIRECTION OF AIR BLAST
AIR-BLAST TUBE TO BE SMOOTH INSIDE. INSIDE DIAMETER 1.91"+0.00"-0.04"

Figure 3. Typical Pitot and air-blast-tube installation for test of aircraft-engine generators

bility of machine characteristics as affected by the brushes. The use of any brush grade usually represents some compromise among these three factors. To match the brush with the machine to secure required performance characteristics is often more important than to match brush and machine to secure maximum brush life.

Types of Tests

It has been found useful to classify tests according to two general types of atmospheric conditions which are generally designated as low altitude or high altitude.

Low altitude covers the ranges of atmospheric conditions usually encountered in test departments and laboratories. The air should be relatively free from chemical, smoke, paint, and any other fumes which are known markedly to affect brush operation. It is proposed that low altitude should include the following listed range of conditions:

1. Altitude from sea level to 1,000 meters.
2. Ambient air temperature from 20 to 40 degrees centigrade.
3. Relative humidity from 20 to 90 per cent.

High altitude is defined as being any atmospheric condition above 1,000 meters in test chambers where the air pressure, dew point, ambient temperature, and air cleanliness are controlled to simulate known high-altitude atmospheric conditions. Generally our experience and the reported results of other investigators have shown that there are no marked changes in brush performance until free-air altitudes of 10,000 to 15,000 feet are exceeded. Because of these experiences and the practical need to limit variations in test conditions to a reasonable minimum, it is proposed that only four general classes of high altitude be adopted as standard. These classes are based upon the estimated atmospheric conditions encountered in or on airplanes, and are proposed to be as shown in Table I.

Classes I, II, and III of Table I are intended to apply to all totally enclosed or self-ventilated machines. Class IV may apply to either self-ventilated or rammed-air-ventilated machines located in the engine nacelles.

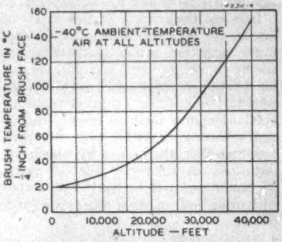

Figure 4. Brush temperature versus altitude

Many machines without adequate ventilation for cooling brushes and commutator at high altitudes soon reach critical temperatures as altitude is increased

Conclusions

The test procedures listed in the appendix have been worked out as a result of conducting some 600 tests on small a-c and d-c machines for aircraft use over a range of from six watts to four horsepower, and from 2,500 to 15,000 rpm. When these methods are followed, it is believed that brush life will be consistent within the range of perhaps two to one on duplicate machine.

Table I

Atmospheric Conditions	Class I Free Air External	Class II Fuselage—Air Within the Airplane	Class III Supercharged Compartment	Class IV Engine Nacelle
Altitude in feet	40,000	40,000	15,000	40,000
Dew point in degrees centigrade	−50 to −80	−50 to −80	−50 to −80	−50 to −80
Ambient-temperature air in degrees centigrade				
(*a*). Surrounding the machine	−40	0	0	+38
(*b*). Rammed air 6 inches from machine intake ports				−30*
Rammed-air pressure (Sum of velocity and pressure head measured with Pitot tube at least 6 in. away from machine intake ports. See Figure 2)				6-in. water*

* Conditions do not apply to self-ventilated machines.

It is very important, however, to note all differences and conditions which may have a bearing on brush performance, so that if later tests give different results, the reason for the difference may be traced.

In studies of brush application, the individuality of different machine designs makes over-all solutions improbable. However, it is believed that close agreement can be obtained by different observers at different times and places on the basic question, "What is a good brush for the particular application?" if the "Brush-Qualification Test Procedure" listed in the appendix is followed. It is proposed that these test procedures and "Types of Tests" should be combined suitably to form a brush test code.

The testing of brushes for life and performance involves two distinct types of measurements. These are the reasonably exact mechanical, electrical, and thermal measurements; and the relatively inexact observational measurements for which the accuracy and usefulness largely are dependent upon the observer's experience in studying brush phenomena. It does not seem likely that sufficient exact measurements can ever be made to provide the complete answer regarding the over-all suitability of the brush for the application. However, meticulous attention to test procedures reduces the usual degree of dependence upon inexact observational measurements, reduces the hazards of possible wrong decisions in the selection of brushes, and greatly improves the usefulness of observational measurements because of the sound background of other factors on which they are based.

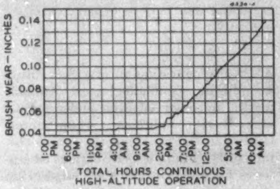

Figure 5. Brush wear versus time

Quick changes to a new greatly increased rate of wear often are encountered at high altitude

Appendix. Brush-Qualification Test Procedure

The following specific rules have been found desirable in conducting brush tests:

1. INITIAL INSPECTION

Carefully check the machine to make sure it has not been damaged mechanically in any important particular. Make sure that balance is satisfactory as evidenced by acceptable vibration, that end-play adjustment is correct, that brushes are free in brush holders with springs in proper place and with brush pressure correctly adjusted, and that commutator or ring run-outs on bearings meet standard limits for the equipment.

2. Preparation for Test

On freshly ground, turned, or polished commutators or rings, operate units approximately five hours at light loads to obtain a satisfactory brush seat and to establish approximately normal brush film on commutators, rings, or other current collecting surfaces.

Many machines will not give satisfactory performance characteristics until the brushes are seated properly. It generally is desired to have brushes seated 100 per cent before beginning tests, but where this is not practical due to limited time, brush seats equal to or better than those shown in Figure 1 have been found satisfactory. All brushes must be seated 75 per cent of face area and 100 per cent of brush thickness for 50 per cent of the brush width. Where there are two or more brushes per stud, all brushes on one stud are to be considered one brush so far as brush seat is concerned.

3. Measurements and Methods

The measurements and observations listed in sections *a*, *b*, *c*, and *d* generally are taken at the beginning of the brush test, at the conclusion of the normal-altitude runs, and again at the conclusion of the high-altitude runs. During continuous-duty test runs, the test readings listed in sections *e*, *f*, *g*, and *h* generally are taken every ten minutes until the machine and associated test apparatus reach reasonably steady operating conditions. Then the intervals may be lengthened up to one hour or more. To insure detection of all significant changes, recording instruments should be used wherever possible.

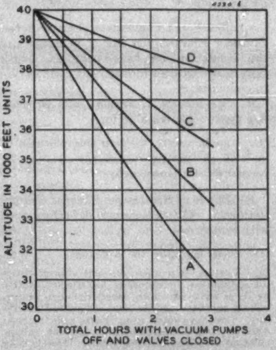

Figure 6. Altitude-chamber leakage control

A and *B* = worst and best conditions over one month interval = adequate tightness to obtain −50-degree-centigrade dew point

C = best obtainable after leaks around screws through the walls were sealed

D = a second chamber with an improved door seal

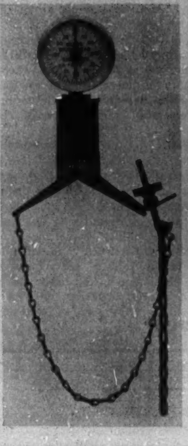

Figure 7 (left). Brush-wear indicating device

Figure 8 (above). Test machine with brush-wear indicator in altitude chamber

a. Commutators. Measure diameter, total gauge, and bar-to-bar run-out in the brush track. Where experience indicates there is any possibility of bar movement, commutator run-outs also should be checked outside the brush track. Note any irregularity across the face of the individual bars, such as "crowning," indicating faulty machining.

Inspect undercuts for any evidence of feather-edge mica, excessive burrs, foreign material.

b. Slip Rings. Check diameters, gauge run-outs, and note any evidences of faulty machining, such as wavy surface.

c. Brush Lengths. Due to the wide variety of shapes, no standard method of designating brush length can be used. Generally measure lengths from the same locations shown on the brush drawing for the particular machine. Measuring to the edges of brushes is often necessary and is satisfactory if micrometers are used very carefully to prevent crushing of the brush edges.

Brushes should be numbered by scriber or other means on a suitable surface for test identification and for location identification for further measurements. Brush wear is often only a few thousandths of an inch during the practical test time used. Therefore, great care in taking measurements is necessary if large percentage errors are to be avoided.

Wherever practical, the use of a continuous-indicating brush-wear device is recommended. The device should have extremely small inertia and have no measurable affect upon brush pressure. A dial-type indicator which has these characteristics, is free from backlash, and is easy to attach to the brush is pictured in Figure 7. A typical installation of the wear gauge in use on a test unit in an altitude chamber is shown in Figure 8.

d. Brush-Spring Pressures. Measure with brush in the location corresponding to its riding position on the commutator or ring. With many mechanisms the pressure may be determined easily by measuring the pull necessary to lift the spring off the top of the brush. On other types where the spring is maintained in position on the brush and the pressure is measured at the brush face, the brush mechanism should be vibrated during these measurements to eliminate the affects of friction in holders. Note any brushes which have sluggish or sticky action in the brushholders.

e. Machine Temperatures. These are taken on all parts where temperature affects the life and reliability of that part, or of the brushes. In general, the minimum points of measurement are as follows:

(1). Two motor brushes (one of each polarity). On small brushes with face area less than one-tenth square inch, the use of five-mil wire thermocouples is recommended. Whatever the brush size may be, it is important that the thermocouples have sufficient flexibility and that they are carefully installed to prevent any interference with brush movement. To permit brush wear during tests, thermocouples usually are installed one-fourth to three-eighths inch away from the brush face. Figure 4 indicates the need for measuring brush temperatures whenever possible.

The brush face temperature is the basic measurement needed. Therefore, thermocouples should be installed as close to the brush face as rate-of-wear conditions during the practical test time will permit. The temperature gradient between brush face and thermocouple location will vary greatly among different brush materials and with different ventilations. The gradient on nonmetallic brushes will be relatively high.

To avoid erroneous thermocouple readings due to stray currents, it is desirable to keep thermocouples electrically insulated from the brushes and the machine. Wire insulating varnishes have been used to insulate the thermocouple tips in a manner that causes very little temperature gradient between the thermocouple and the brush.

(2). Each slip-ring brush.

(3). Each bearing housing as close to the bearing as possible.

(4). Motor case.

(5). Field windings or other stationary windings.

f. Altitude-Chamber Ambient Temperatures. Ventilation conditions in chambers are often far from ideal, because the chambers are relatively small; temperature gradients, air turbulences and flows may exist in connection with the refrigerating systems; and large gradients may exist between the chamber wall and case temperatures of the machine under test.

Check temperature on the chamber walls, in air six inches from the test machine intake air ports, and at as many other points as necessary to determine average air ambient temperature six inches from the test unit. In rammed-air-cooled machines the temperature should be taken in the center of the air duct six inches from the surface of attachment of air duct to the machine. Keep ambient-temperature thermometers appreciably removed from the exhaust air of the test unit.

g. Machine Performance. Take measurement of: input volts, amperes, and watts; speed; output volts, amperes, and watts; output or input torque; field amperes; and any other readings required to determine completely electrical performance of the machine.

h. Altitude. Use altimeter or barometer.

i. Dew Point. Measure at beginning of test and at least every five hours thereafter. The measurement desired is the dew-point temperature of air entering test machine. Accurate values sometimes are difficult to obtain, particularly in altitude chambers. When testing self-ventilated machines in chambers having appreciable leakage, it is important to measure dew point of air near intake of test unit. A method for this is shown in Figure 2.

With negligible leakage, it would be adequate to measure dew point in only the dry-air supply lines to the chamber. Typical leakage-control tests are illustrated in Figure 6. Experience indicates that leakage control to assure low dew point is the most critical problem in high-altitude chamber operation. One cubic foot of leakage air may carry as much moisture into chamber as several thousand cubic feet of air from an alumina dryer.

4. GENERAL FACTORS INDICATING BRUSH SUITABILITY, TROUBLES, AND PROBABLE CAUSES

Observations of the following will assist in judging brushes and diagnosing trouble.

a. Surface Condition of Commutator or Slip Rings

(1). Bar-to-bar burning, uniform or nonuniform.

(2). Wearing out-of-round.

(3). Ring grooving or threading, severity and stability.

(4). Skip burning. Note any evidence which indicates the cause (bad mechanical run-outs, faulty brush mechanisms, bad rotor balance, defective bearings, brush overfilming).

(5). Uneven film.

(6). Indications of polarity spots when collecting alternating current on slip rings.

b. Uniformity of Brush Wear. The following possibilities should be investigated when wear of individual brushes deviates more than plus or minus 20 per cent from average:

(1). Polarity effects of brush material.

(2). Uneven spacing of holders.

(3). Uneven brush-spring pressures.

(4). Brushes stuck in holders or binding of pivoted holders.

(5). Brushes incorrectly positioned (wrong electrical shift).

c. Brush and Commutator Temperatures. Consider effects upon life of bearings and the insulation on brush holders, brush shunts, armature winding, and connections to commutator.

d. Mechanical Strength of Brushes. Are the brushes suitable for:

(1). Type of holder? Consider brush sidewear.

(2). Service vibration conditions? Particularly consider the shunt connections.

e. Effects of Brushes on Machine Characteristics. Check the over-all performance with respect to:

(1). Power input over the load range.

(2). Contact drop where machine characteristics (in addition to commutation) are affected greatly by it, as on dynamotors, speed-regulated motors, voltage-regulator circuits, and self-excited generators. Does the contact drop change sufficiently to disturb voltage regulation or to prevent voltage build-up of self-excited generators? During long test runs make periodic checks to determine any marked changes in contact drop.

(3). Commutating ability for expected manufacturing variations of a particular class of machines

(4). Consistency of performance:
From zero to high current densities.
From low to high humidity.
From lowest to highest operating temperature.

(5). Brush riding characteristics, as indicated by ripple voltage, radio-interference filter requirements, and amount of brush chatter over operating range.

CARBON BRUSH TEST REPORT

Tested at ______ Works in Bldg. ______ Machine Model ______

Serial Number of Test					
Machine Serial Number					
*Ring or *Commutator Metal					
Where Was Metal Obtained?					
Brush Grade and Treatment					
Where Were Brushes Obtained?					
Atmosphere (air, nitrogen, etc.)					
Refrigerant (dry ice, Freon, etc.)					
Dew Point (°F or °C)					
Altitude—Feet					
Machine Speed in RPM					
Load Amperes					
Brush Density—Amps/Sq In.					
Field Current—Amps					
Ambient Temp (°F or °C)					
"Air In" Temp (°F or °C)					
Brush Temp (°F or °C)	Max.-Min.				
	Average				
Ring or Comm. Temp (°F or °C)					
Spring Force—Oz	Ave. Initial				
	Ave. Final				
Friction Watts	Max.-Min.				
	Average				
Brush Contact Drop	Max.-Min.				
	Average				
Hours Tested—Total					
Hours Tested at Altitude					
Actual Brush Wear—Inches					
Indicated Brush Life per ¼"—Hours					
Ring or Comm. Wear (Diam) Inches					
Date of Test					

REMARKS: Commutation, Film Condition, Vibration, Ventilation, Reason for Stopping Test, etc.

*Indicate which   Date   Signed

Figure 9. Brush-test report

5. General Precautions for High-Altitude Tests

a. Altitude. Maintain within plus or minus 1,000 feet.

b. Ambient Temperature. Maintain within plus or minus five degrees centigrade.

c. Contamination. Admit fresh air into the altitude chamber continuously to avoid accumulation of contaminating vapors.

The vapors from bearing greases, incompletely cured paints, and insulating varnishes of new machines have greatly improved the high-altitude brush wear in specific chamber tests, until the most volatile constituents evaporated and insufficient contamination remained to lubricate the brushes. On totally enclosed or only slightly ventilated machines, this self-contamination may be pronounced, and there is often a great difference in brush performance on new and old machines.

When practical, recirculation should be avoided and only fresh air admitted to the test machine. This can be done readily with pressure-ventilated machines, but is not feasible for most self-ventilated units because of interference with ventilation and radiation. Experience with recirculating ventilated chambers indicates that contamination usually becomes negligible if the chamber air is changed five to ten times per hour. The recommended frequency of changes depends on the relative size of chamber and test units.

A chamber with recirculation should not have thermal insulation, braid-covered leads, varnish-cloth tape, wooden platforms, or other absorbent materials inside. It should be cleaned thoroughly before every test. Steam cleaning followed by a bake to dry out all moisture has been found satisfactory. The chamber should be flushed with dry air before refrigerating. The walls and cooling coils, if any, should not accumulate objectionable frost during an altitude test.

d. Need for Long Tests. On high-altitude trials of less than one hour, indicated brush life may be many times that obtained on a continuous-run-to-destruction. Some altitude-protected brushes may operate well for ten hours or more, and then suddenly dust, overheat, become unstable, cause excessive friction, or high-contact drop. It is important to take relatively long trials as may be noted in Figure 4.

e. Initial Equilibrium Conditions. High-altitude conditions usually are established before starting the test machines, thereby providing a more severe test for the brushes. Climb and dive cycles sometimes must be simulated, but unless otherwise specified, equilibrium conditions should be established first.

6. Duration, Repetition, and Sequence of Tests

Tests should be taken at rated speed, voltage, load (or load cycle), or at defined specification conditions.

a. Duration of Low-Altitude Tests. A qualification life test should be a minimum of 300 hours, or 25 per cent of guaranteed life, whichever is shorter.

b. Duration of High-Altitude Tests. A qualification life test should be a minimum of 30 hours, or 25 per cent of guaranteed life, whichever is shorter. If necessary, the test may be divided into minimum runs of 15 hours or more with intervening low-altitude trials.

c. Repetition of Tests. Because of variations in machine and test conditions, the results with three or more different units should be averaged to obtain representative brush life for a specific machine design. When only a single machine is used for tests, the results from two or more life tests should be averaged to obtain representative brush life for the particular machine.

d. Sequence of Tests. Low-altitude tests usually are taken prior to high-altitude tests because:

(1). The film formed at low altitude may influence the high-altitude performance. Most units operate initially at low altitudes in actual service.

(2). Some brushes lose their high-altitude protection during extended runs. This characteristic might not be revealed by a short-altitude test only.

(3). Mechanical deterioration of machines adversely effects brush performance. Chamber tests should simulate such unfavorable conditions as are likely to exist in flight.

(4). The more volatile components of lubricants, insulating varnish, and motor paint tend to be driven off during extended low-altitude runs. This reduces contamination of the altitude chamber.

7. Methods of Reporting Test Results

To assist in reporting all significant facts, a standard test data form similar to Figure 9 is recommended.

Solderless Terminals

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IN the last few years solderless terminals have found an increasingly wide application. The fields of application include aircraft, shipboard, communication, and general industrial work. One of the most important reasons for their increasing use is the substantially decreased labor required in their application, as compared to soldered terminals. The use of solderless terminals also gives assurance of a greater degree of uniformity with respect to both mechanical and electrical properties for the terminal conditions of the electric circuit. Furthermore, these terminals are more readily adaptable for such improvements as insulation supports for the wire insulation to which the solderless terminal is crimped and the use of a preinsulated terminal. In general, the use of solderless terminals in both manufacture and application permits machine production and uniformity as compared to handwork necessary for soldered terminals.

Description

A type of solderless terminal is shown in Figures 6 and 10. These terminals consist of a tongue or lug for terminal-block connection, and an internally serrated barrel with sleeve for crimping to the wire. In general, the stranded wire with insulation stripped off the end is inserted for the full length of the barrel, and then two adjacent lateral crimps are applied. This crimping may be accomplished by either a hand tool or a machine press. An enlarged picture of this terminal through one of the crimp sections for a 19-strand wire is shown in Figure 1. The section has been etched with a solution of ammonium hydroxide and concentrated hydrogen peroxide to indicate clearly the individual strands of wire following crimping. Sufficient pressure is exerted in the crimping operation to give a flow of metal, so that maximum contact area between the strands and between strands and terminal barrel is obtained. This gives maximum electrical conductivity for a given volume of metal. Furthermore, this would give maximum tensile strength for the terminal, and also would give a minimum number of longitudinal channels for entrance of corrosive liquids or condensates. In general, the crimp is designed to give a tensile strength of the wire to the crimped joint approximately equal to that of the stranded wire itself. Comparative tensile-strength test results are shown in Figure 2. In these tests failure was obtained by breaking of the stranded wire itself rather than by the stranded wire pulling out of the crimped joint. The differences between the tensile strengths of the stranded wire itself and of the stranded wire when crimped to a terminal is due to different methods of terminating the wire in the two cases, with better equalization of individual strand stress being possible in the first case.

Soldered Terminals

Since soldered terminals have been used for a long time, it is perhaps natural that any other method of adapting a terminal to a wire would be analyzed in comparison with the performance of a soldered terminal. Certain of the inherent difficulties in obtaining a satisfactory soldered joint are not generally appreciated. For electric conductors

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tin–lead solder is generally used with either copper terminals or tinned-copper terminals and wire. A properly soldered joint is dependent, however, on the simultaneous achievement of cleanliness of metal surfaces and sufficient heating of the parts to be soldered. A successfully soldered joint involves the actual alloying of the soldered parts with the solder itself. Insufficient heat will cause the solder only to wet the metal, but not to alloy with it. Since there is no difference in the appearance of the two types of soldered joints, it is extremely difficult to determine whether two metals are soldered properly. The presence of large beads of solder on a joint does not mean necessarily that the metals have been soldered properly. In fact, unnecessary solder prolongs the cooling time and enhances the possibility of movement of the parts to be joined, causing the solder to crystallize, thereby weakening the joint.

In general, the electrical conductivity stability of a properly soldered joint is practically constant under long-time corrosion conditions. However, improperly soldered joints have shown relatively large increases in resistance under corrosion conditions. Such increases may be of the order of a few hundred per cent or more, depending upon the degree of departure from ideal soldering conditions.

Test Procedures and Measurements

One of the most important electrical characteristics of a terminal or electric connection is the stability of the electrical conductivity. This may be determined by an electrical-resistance measurement. The initial resistance is of importance, since it is a measure of the electrical efficiency of the contact as a conducting medium. The change in resistance under corrosion conditions is of importance, since it will determine the electrical stability of the contact under operating conditions. This stability under operating conditions is particularly important, since any great increase in resistance generally gives increased operating temperatures and a consequent increase in the corrosion rate.

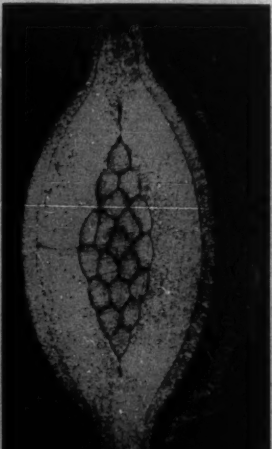

Figure 1. Enlarged picture of crimped solderless terminal

Since corrosion plays a large part in determining the relative electrical stability of a solderless terminal, the design and metallic composition of a terminal must of necessity preclude any extensive corrosion. The terminal must be able to withstand operating conditions in which it may be exposed to high humidities, natural waters, chemical salts and brines, and high temperatures. The terminal must be crimped in such a manner that no corrosion occurs because of cracking or fatigue in the metal. The metal used in the construction of the terminal must be such as to obviate any electrochemical corrosion due to galvanic action between the terminal and the wire.

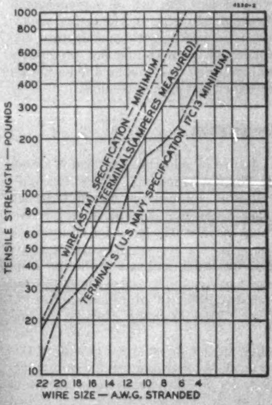

Figure 2. Tensile strengths—terminals and wire

Electrical measurements of resistance of solderless terminals of the type described in this paper have been made under the following types of corrosion conditions:

1. Continuous four per cent sodium-chloride spray.
2. Continuous ten per cent sodium-chloride spray.
3. Continuous four per cent sodium-chloride with 0.1 per cent sodium-hydroxide spray.
4. Alternating four per cent sodium-chloride spray and circulating-air oven at 100, 133, 166, and 200 degrees centigrade.
5. Alternating four per cent sodium-chloride and 0.1 per cent sodium-hydroxide spray with 100 degrees centigrade circulating-air oven.
6. Relative humidity of 85 per cent alternating with 100 degrees centigrade circulating-air oven.
7. Alternation of 100 degrees centigrade circulating-air oven, four per cent sodium-chloride spray, and vibration test at $^1/_{32}$-inch amplitude at a frequency of 2,000 cycles per second—vibration test made to give flexing of stranded wire relative to terminal.
8. Sulphur dioxide vapor.
9. Alternating and direct currents corresponding to approximately three times normal rated currents for periods of three-fourths hour, alternating with different salt-spray tests.

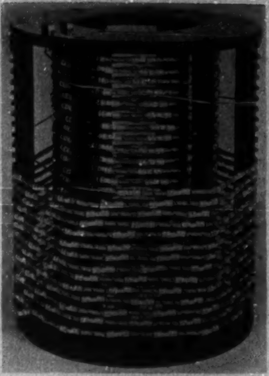

Figure 3. Assembly of 1,000 solderless terminals for electrical measurements during corrosion tests

The foregoing corrosion tests have been made on different terminals for varying periods of time. In certain cases they have been continued for a period of 150 days. It is believed that such corrosion conditions are very much more severe than those normally encountered in any application, but they may be considered as accelerated tests. In addition, they indicate the particular characteristics of a given type of corrosion or the relative efficiency of different types of terminals, with respect to either materials or construction.

Electrical measurements of resistance have been made using both alternating and direct current, with the currents varying from ten microamperes up to those substantially greater than the normal rating of the wire. Resistance

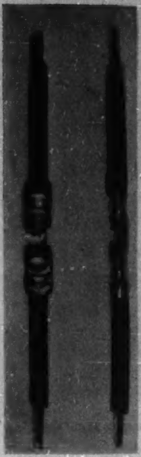

Figure 4. Detail of connector

measurements of this type generally fall in the range from one milliohm to one microhm. The measuring circuits that have been used include the Kelvin–Thomson bridge, potentiometer, and comparison methods. In general, any of these methods may be used with either alternating or direct current. For relatively low resistances with low currents the use of alternating current is to be preferred, in view of the facility with which sensitivity may be increased by amplification. The use of alternating current also eliminates difficulties due to thermal and contact-voltage effects. For the measurement of very low voltages, the use of a tuned circuit such as a wave analyzer is desirable. In general, any of the methods will give a sufficient precision and accuracy with appropriate equipment and calibration.

Test Results

Measurements of the electrical resistance of solderless terminals have been made using different types of corrosion tests with different contact metals over very wide ranges of current, under different temperature conditions for both the measurements and the corrosion tests.

RESISTANCE OF SOLDERLESS TERMINALS OVER A WIDE RANGE OF CURRENT

Measurements of electrical resistance of solderless terminals over a range of current from ten microamperes to ten amperes in decade steps are given in Table I. These measurements were made on an assembly of 500 connectors as shown in Figure 3. The connector is shown in Figure 4; each connector includes two regular crimped solderless terminals with a short strip or link between the individual terminals. Thus, the assembly in Figure 3 consists of a series connection of 1,000 solderless terminals or crimped joints. The connectors are number 16–14 with 19-strand number-14 American Wire Gauge insulated tinned-copper wire. The connectors as shown in Figures 4 and 5 are joined with a short length of insulated wire to give a seven-eighths-inch length of wire between adjacent faces of the connectors. The resistances in ohms in Table I are indicated by R_1 in Figure 5, and include one crimp plus a $^7/_{16}$-inch length of stranded number-14 wire plus one half of the link between connectors. The type of corrosion used in this test consisted of a combination of four per cent salt spray for a period of 24 hours followed by an equal period in a circulating-air oven at 100 degrees centigrade.

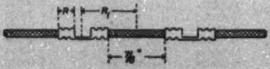

Figure 5. Diagram of measured resistances—connector

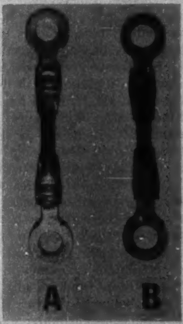

Figure 6. Solderless terminals for resistance measurements during corrosion tests

The results in Table I were measured at room temperature (20 degrees centigrade), and at an elevated temperature approximating 100 degrees centigrade. In the current range from one milliampere to ten amperes the resistance measurements were made using alternating current at a frequency of 60 cycles per second, and below this current range they were at a frequency of 100 cycles per second. The test results show that the resistance of the solderless terminals is independent of the current over the range from ten microamperes to ten amperes. This applies to resistance measurements at both room temperature and at 100 degrees centigrade. In this case the corrosion test was continued for a period of 108 days, which would normally be considered an extreme in terms of standard accelerated corrosion tests.

COMPARISON OF TINNED-COPPER SOLDERLESS TERMINALS AND WIRE WITH CORROSION-RESISTANT-TREATED SOLDERLESS TERMINALS

The resistance measurements shown in Tables II and III give a comparison of resistance stability during corrosion of a tinned-copper terminal crimped to tinned-copper wire (Table II) with a corrosion-resistant-treated copper terminal and tinned-copper stranded wire (Table III). These resistance measurements were made using two solderless terminals mounted back to back, as shown in Figure 6. The terminals are of a type with an insulation support. The terminals are number 22-16 with seven-strand number-18 insulated wire. The resistances in ohms given in Tables II and III are indicated by R_1 in Figure 7 and include two crimped or solderless terminals plus five-eighths inch of wire plus a portion of each lug of each terminal. The corrosion cycle consisted of passing 15 amperes alternating current,

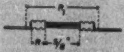

Figure 7. Diagram of measured resistances—terminal

Figure 8 (left). Extent of corrosion—28-day salt-spray test

Figure 9 (right). Series connection of solderless terminals

Table I. Resistance of Solderless Connectors at Currents Indicated

Duration of Corrosion Test—Days	Microamperes 10	Microamperes 100	Milliamperes 1	Milliamperes 10	Milliamperes 100	Amperes 1	Amperes 10
Resistance Measurements at Room Temperature (20 Degrees Centigrade)—Ohm							
0	0.00019	0.00019	0.00019	0.00019	0.00018	0.00018	0.00018
8	0.00019	0.00019	0.00019	0.00019	0.00019	0.00019	0.00019
18	0.00019	0.00020	0.00020	0.00020	0.00020	0.00020	0.00020
28	0.00019	0.00020	0.00020	0.00020	0.00020	0.00020	0.00021
33	0.00019	0.00020	0.00020	0.00020	0.00020	0.00021	0.00021
42	0.00020	0.00020	0.00020	0.00020	0.00020	0.00020	0.00020
49	0.00020	0.00021	0.00021	0.00021	0.00021	0.00021	0.00021
55	0.00021	0.00021	0.00021	0.00022	0.00021	0.00021	0.00021
62	0.00021	0.00022	0.00022	0.00022	0.00021	0.00021	0.00021
69	0.00021	0.00022	0.00023	0.00023	0.00022	0.00022	0.00021
76	0.00022	0.00024	0.00024	0.00024	0.00022	0.00022	0.00022
83	0.00025	0.00025	0.00025	0.00025	0.00022	0.00022	0.00022
90	0.00025	0.00025	0.00025	0.00025	0.00023	0.00023	0.00023
98	0.00024	0.00024	0.00024	0.00024	0.00023	0.00023	0.00023
108	0.00024	0.00023	0.00023	0.00023	0.00024	0.00024	0.00024
Resistance Measurements at Approximately 100 Degrees Centigrade—Ohm							
49	0.00028	0.00028	0.00028	0.00028	0.00028	0.00028	0.00027
55	0.00028	0.00028	0.00028	0.00028	0.00028	0.00028	0.00027
62	0.00028	0.00028	0.00028	0.00028	0.00028	0.00027	0.00026
69	0.00028	0.00028	0.00028	0.00028	0.00028	0.00028	0.00028
76	0.00028	0.00029	0.00028	0.00028	0.00028	0.00028	0.00028
83	0.00028	0.00028	0.00029	0.00028	0.00028	0.00028	0.00028
90	0.00030	0.00030	0.00030	0.00030	0.00029	0.00029	0.00029
98	0.00030	0.00030	0.00030	0.00030	0.00029	0.00029	0.00029
108	0.00030	0.00030	0.00020	0.00020	0.00032	0.00032	0.00031

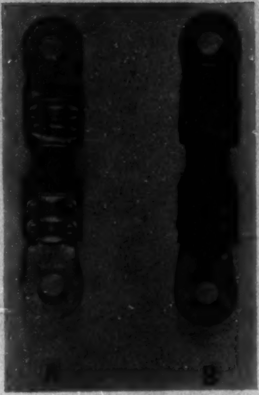

Figure 10. Solderless terminals

A—Before corrosion test
B—After 28-day corrosion test

60 cycles per second, through the terminals for 45 minutes, followed by $7^1/_4$ hours in ten per cent salt spray, after which the salt spray was shut off, although the samples were allowed to remain in the salt-spray chamber for the remainder of the 24 hours. Certain standard specification tests have used a similar corrosion cycle. It has been found for the high-current portion of the corrosion cycle that, if the tongue of the corroded terminals is adequately cleaned for contact purposes, no perceptible heating of the terminals will ensue. However, if contact with corroded terminals is made without cleaning of the contact points where current enters the terminals under test, excessive heating of the terminals will occur. These temperatures may be in the range of 100 to 200 degrees centigrade or higher, depending upon the extent of the corrosion where contact is made. However, such heating is at the contact points where current is brought into the terminal assembly and is not due to the resistance of the solderless terminal itself. For these particular tests groups of pairs of terminals, shown in Figure 6, were connected in series by bolted connections, as shown in Figures 8 and 9. Figures 8 and 9 show the extreme corrosion products formed after a period of 28 days or 28 corrosion cycles. A considerable portion of the corrosion products that are shown results from the plated iron machine screw and nut that were used to bolt the terminals together. The resistance measurements given in Tables II and III were made across each pair of terminals, as shown in Figure 6. The results indicate a practically constant electrical resistance for the corrosion-resistant terminals throughout the corrosion test, as compared to an approximate 75 per cent increase for the regular tinned-copper terminals. If allowance is made in the resistance measurements for the approximate resistance of the connecting wires between the two terminals, and of the resistance for a portion of the tongue of the terminals, the terminal resistance itself, as indicated by *R* in Figure 7, is of the order of 0.00008 ohm for the tinned-copper terminals, and 0.00005 ohm for the corrosion-resistant-treated terminals. Thus, the corrosion-resistant-treated terminal not only shows greater conductivity stability during the corrosion test, but also gives a decreased initial resistance per crimped or solderless terminal. The corrosion conditions existing in this particular test are undoubtedly very severe, and probably are very much more severe than would be encountered in any practical application.

Conductivity Stability of Relatively Large Solderless Terminals Under Corrosion

The foregoing measurements of the electrical resistance of solderless terminals during corrosion have been made on stranded wire sizes 14 and 18 with corresponding terminals. Tests also have been made on stranded wire sizes 4 and 6 with corresponding terminals. These tests have been made on two terminals mounted back to back as shown in Figure 10. Figure 10, part *A*, shows the terminal before the start of the corrosion test, and Figure 10, part *B*, shows the terminal after 28 days' salt-spray corrosion.

Figure 11 shows the resistance of the terminals and wire as a function of corrosion time or cycles during the test and includes:

Graphs 1 and 3, tinned-copper terminals with tinned-copper wire.

Graphs 2 and 4, corrosion-resistant-treated copper terminals with tinned-copper wire.

Graphs 1 and 2, number 6 terminals with stranded (181 strands) wire.

Graphs 3 and 4, number 4 terminals with stranded (132 strands) wire.

The corrosion cycle consisted of eight hours of four per cent salt spray and 16 hours of circulating-air oven at 100 degrees centigrade. The resistances are measured in a manner corresponding to the diagram shown in Figure 7, and in-

Table II. Resistance of Solderless Terminals—Tinned-Copper Terminal—Tinned-Copper Stranded Wire

Duration of Corrosion Test—Days	Ohm No. 1	Ohm No. 2	Ohm No. 3	Ohm No. 4	Ohm Average
0	0.00046	0.00046	0.00052	0.00046	0.00048
2	0.00046	0.00046	0.00052	0.00046	0.00048
3	0.00048	0.00047	0.00054	0.00050	0.00050
8	0.00050	0.00051	0.00054	0.00050	0.00051
12	0.00057	0.00052	0.00054	0.00055	0.00054
14	0.00055	0.00052	0.00054	0.00056	0.00054
16	0.00056	0.00052	0.00056	0.00056	0.00055
23	0.00072	0.00051	0.00054	0.00061	0.00060
28	0.00096	0.00062	0.00096	0.00067	0.00080

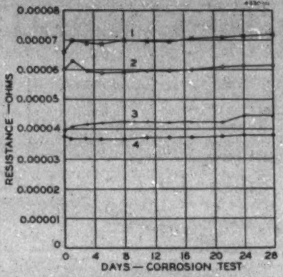

Figure 11. Electrical resistance–corrosion time of corrosion-resistant tinned terminals

Table IV. Resistance of Solderless Terminals—Strand to Terminal

Strand Number	Duration of Corrosion Test—Days						
	0	13	27	34	44	50	57
1	0.0036	0.0037	0.0038	0.0036	0.0039	0.0035	0.0035 ohm
2	0.0040	0.0040	0.0041	0.0045	0.0041	0.0039	0.0038 ohm
3	0.0038	0.0039	0.0041	0.0041	0.0041	0.0039	0.0039 ohm
4	0.0038	0.0038	0.0039	0.0040	0.0040	0.0037	0.0035 ohm
5	0.0038	0.0038	0.0038	0.0037	0.0039	0.0037	0.0036 ohm
6	0.0038	0.0039	0.0038	0.0037	0.0041	0.0039	0.0037 ohm
7	0.0037	0.0038	0.0038	0.0037	0.0039	0.0036	0.0035 ohm
8	0.0041	0.0043	0.0044	0.0044	0.0045	0.0042	0.0041 ohm
9	0.0041	0.0044	0.0045	0.0044	0.0046	0.0044	0.0042 ohm
10	0.0038	0.0041	0.0043	0.0043	0.0042	0.0041	0.0039 ohm
11	0.0038	0.0042	0.0043	0.0045	0.0043	0.0041	0.0039 ohm
12	0.0041	0.0043	0.0044	0.0044	0.0045	0.0043	0.0041 ohm
13	0.0039	0.0041	0.0042	0.0041	0.0043	0.0040	0.0039 ohm
14	0.0039	0.0043	0.0043	0.0042	0.0044	0.0041	0.0039 ohm
15	0.0040	0.0046	0.0045	0.0045	0.0046	0.0044	0.0042 ohm
16	0.0039	0.0043	0.0043	0.0043	0.0044	0.0041	0.0039 ohm
17	0.0039	0.0043	0.0043	0.0044	0.0044	0.0042	0.0040 ohm
18	0.0038	0.0042	0.0043	0.0043	0.0044	0.0041	0.0040 ohm
19	0.0040	0.0043	0.0045	0.0045	0.0045	0.0043	0.0041 ohm

clude two terminals plus one-half-inch to five-eighths-inch wire, plus portions of the tongue of each terminal. The results show relatively good conductivity stability for both sizes of wire and for both types of terminals, the best stability being obtained with the corrosion-resistant-treated terminals. Furthermore, the resistance of the corrosion-resistant terminal is significantly less than for the tinned-copper terminal. Making appropriate allowance for the resistance of the wire and tongue in the measurements given in Figure 11 gives a value for the initial barrel resistance (*R* in Figure 7) of the number-4 and number-6 tinned-copper terminals of approximately 0.000014 and 0.000009 ohm, respectively, and of the number-4 and number-6 corrosion-resistant terminals 0.000011 and 0.000007 ohm, respectively.

Resistance Measurements on Individual Strands of a Stranded Wire With a Solderless Terminal

Previous resistance tests on solderless terminals have been made from one terminal to another terminal. In this case, however, resistance measurements have been made from each strand of a stranded wire to the solderless terminal crimped to the group of stranded wires. The purpose of these tests is to determine the uniformity of the resistances from the different strands to the terminal. Measurements were made on an assembly, as shown in Figure 12, with the results being given in Table IV. A wire with 19 strands was used in which strands 1–12 inclusive were in the outer layer, 13–18 inclusive were in the intermediate group, and 19 was the central strand. The results in Table IV give the resistance measurements made on each strand at different lengths of time during a 57-day corrosion test. The corrosion test consisted of a cycle of 24 hours in four per cent salt spray followed by 24 hours in a 100 degrees centigrade circulating-air oven. In measurements of this type it is not possible to obtain a high degree of uniformity among the individual measurements shown in Table IV in view of the high resistance of the individual strand and the variable length of the individual strands. However, the results do indicate both a reasonable uniformity of individual-strand resistance measurements and a satisfactory conductivity stability for the individual strands.

Effect of Different Contact Metals With Copper

Previous test results have been obtained using regular tinned-copper terminals crimped to tinned-copper wire. The results in Figure 13 gave a comparison of the electrical resistance as a function of corrosion time, using graph 1, copper terminal with tinned-copper stranded wire; graph 2, tinned-copper terminal and tinned-copper stranded wire; graph 3, corrosion-resistant-treated terminal and tinned-copper wire. These tests were made using number-22-16 terminals with seven-strand number-18 wire. The measurements were made on two terminals mounted back to back, as shown in Figure 6, and include the resistance R_1 as indicated in Figure 7. The corrosion-test cycle consisted of four per cent salt spray for eight hours, followed by 16 hours in a circulating-air oven at 100 degrees centigrade. The test was continued for a period of 28 days. The curves in Figure 13 show that the greatest increase in resistance was obtained with the copper terminal on tinned-copper wire (graph 1). This increase in resistance may be considered to be due to the oxidation of the copper and to the galvanic action resulting from the two different contact metals. The greater conductivity stability of the tinned-copper terminal to the tinned-copper wire (graph 2) as compared to graph 1, copper terminal to tinned-copper wire, is due both to the absence of any electromotive force between the different contact metals and to the greater chemical stability of the tin. Graph 3 shows the improved conductivity stability of the corrosion-resistant-treated copper terminal and tinned-copper wire. This is due to the fact that both corrosion and contact electromotive forces due to different metals have been effectively eliminated in this terminal. It is also of interest in studying Figure 13 to note that the terminals with the greater conductivity stability with corrosion time have the lower initial resistance. Since these differences in the initial resistances are due to differences in the barrel resistances, *R* of Figure 7, and since the initial resistances include some wire resistance, the differences between the barrel resistances are proportionally much greater than those indicated.

Table III. Resistance of Solderless Terminals—Corrosion-Resistant Treated Copper Terminal—Tinned-Copper Stranded Wire

Duration of Corrosion Test—Days	Ohm						
	No. 1	No. 2	No. 3	No. 4	No. 5	No. 6	Average
0	0.00043	0.00042	0.00043	0.00042	0.00043	0.00043	0.00043
1	0.00043	0.00043	0.00043	0.00042	0.00043	0.00043	0.00043
4	0.00043	0.00043	0.00043	0.00042	0.00042	0.00043	0.00043
6	0.00040	0.00040	0.00040	0.00038	0.00040	0.00040	0.00040
8	0.00041	0.00041	0.00043	0.00038	0.00041	0.00041	0.00041
11	0.00041	0.00041	0.00042	0.00040	0.00040	0.00042	0.00041
13	0.00042	0.00040	0.00042	0.00038	0.00040	0.00041	0.00040
17	0.00041	0.00041	0.00040	0.00039	0.00040	0.00041	0.00040
19	0.00041	0.00040	0.00041	0.00039	0.00041	0.00040	0.00040
21	0.00041	0.00041	0.00042	0.00039	0.00040	0.00042	0.00041
28	0.00041	0.00041	0.00041	0.00036	0.00038	0.00041	0.00040

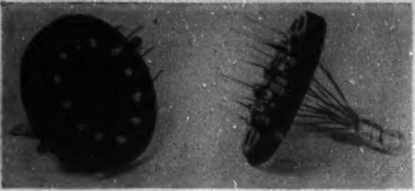

Figure 12. Assembly — electrical measurements strand to solderless terminal

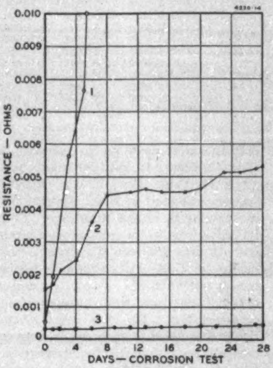

Figure 14 (below). Electrical resistance—corrosion time of different contact metals—aluminum wire

The Effect of Different Contact Metals With Aluminum

The curves in Figure 14 give the relative conductivity stability of different types of terminal metals with stranded aluminum wire. Graph 1 shows the results with a tinned-copper terminal with aluminum wire; graph 2 shows an aluminum terminal with aluminum wire; graph 3 shows a specially treated terminal and aluminum wire. These tests were made using number-22-16 terminals with seven-strand number-18 wire. The terminals were mounted back to back as shown in Figure 6. The resistance in ohms in Figure 14 is shown by R_1 in Figure 7 and includes portions of each lug plus two crimps, plus five-eighths inch of wire. The corrosion cycle consisted of eight hours of four per cent salt spray and 16 hours of circulating-air oven at 100 degrees centigrade. The greatest increase in resistance is obtained with the tinned-copper terminal to the aluminum wire, since in this case the increase in resistance is due both to galvanic action because of different contact materials and to the oxidation of the aluminum. The aluminum terminal to the aluminum wire shows an initial rapid increase in resistance, because of the rapid formation of relatively high-resistance aluminum oxide. However, the rate of aluminum-oxide formation decreases as a protective coating is formed on the wire, thus giving somewhat greater resistance stability after eight days of the corrosion test. The greatest resistance stability is obtained with the treated terminal and aluminum wire, since in this case galvanic action is eliminated, and the chemical corrosion stability has been increased.

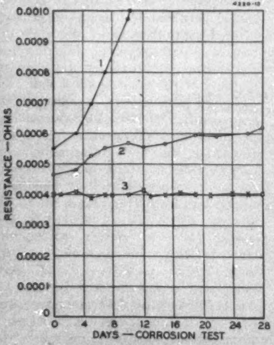

Figure 13. Electrical resistance–corrosion time of different contact metals—tinned copper wire

The Effect of Temperature During the Corrosion Cycle

The curves in Figure 15 give the relative conductivity stability of different types of terminals when subjected to a corrosion cycle consisting of salt spray alternating with circulating-air oven at various temperatures. The graphs marked *A* are the results obtained using tinned-copper terminals with tinned-copper wire; the graphs marked *B* are corrosion-resistant-treated terminals with tinned-copper wire. The temperatures used with the tinned-copper terminals and wire are 25, 66, 100, 133, 166, and 200 degrees centigrade. The temperatures used with the corrosion-resistant-treated terminals were 100, 133, and 166 degrees centigrade. These results were obtained with number-22-16 terminals and seven-strand number-18 wire with the terminals mounted back to back as shown in Figure 6. The resistance in ohms in Figure 15 is indicated by R_1 in Figure 7 and includes portions of each lug plus two crimps plus five-eighths inch of wire. The corrosion cycle consisted of eight hours of four per cent salt spray followed by 16 hours in a circulating-air oven at the temperatures indicated. The results of electrical-resistance measurements as a function of corrosion time show very good stability at all temperatures for the corrosion-resistant-treated terminals. For the tinned-copper terminals, the conductivity stability is satisfactory for all temperatures, except the highest ones. The effects of the high temperature are both an increase in the corrosion rate, and the formation of oxides having relatively high specific resistance.

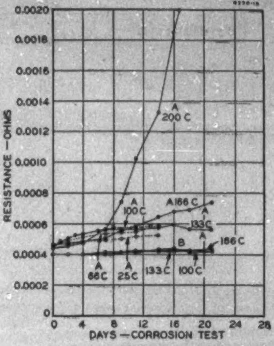

Figure 15. Electrical resistance–corrosion-time effect of different temperatures

Conclusions

1. Solderless terminals may be designed to be both chemically and electrically stable under corrosion conditions of salt spray and heat. It is believed that these corrosion conditions represent reasonably severe operating conditions.

2. The contact metals should have maximum chemical corrosion stability and a minimum electropotential between them for improved performance.

3. The tensile strength of the stranded wire to the crimped joint of the solderless terminal may be made approximately equal to that of the stranded wire when terminated in a solderless terminal.

4. The electrical resistance of the solderless terminal is independent of the magnitude of the current. Tests made on number-18 American Wire Gauge tinned-copper stranded wire with corresponding solderless terminal gave electrical resistance independent of the current over the range from ten microamperes to ten amperes at both room temperature and 100 degrees centigrade.

5. The electrical resistance of the solderless terminal is less than that for an equal length of the corresponding stranded wire. This should insure satisfactory operating performance with respect to temperature rise under load.

Electrical Control in Automatic Pilots

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Synopsis: Engineers generally have been encouraged to attack new seemingly difficult and complicated problems by adapting the general principles utilized in their peacetime work to specific situations arising from the changed methods of modern warfare. This is well exemplified in the solution of aircraft-automatic-control problems.

A summary of automatic-pilot principles and a description of some of the unique and less understood features of a specific modern design are discussed in this paper. It is of especial interest at the present time, because many manual operations of flight and engine control are being studied with a view to making them automatic, and, because as this trend becomes more pronounced, the interconnection of these automatic features will make the problems of each interrelated.

THE automatic pilot is, as its name implies, a mechanism capable of automatically piloting an aircraft and maintaining it on a course, despite disturbances in the surrounding air.[1] Such terms as "relief pilots," "maneuvering pilots," "hard or soft pilots," are used to classify apparatus which accomplishes this result. Relief pilots are intended to relieve the strain on the human pilot mainly during straight level flight and are more simple than the maneuvering type of pilot. Hard pilots are designed to hold a given course or attitude rigidly without consideration for the comfort of the passengers and crew. The soft pilot, on the other hand, holds the general course and attitude but without uncomfortably jarring the personnel. Some designs of automatic pilots incorporate all the features of relief, maneuvering, and hard or soft control.

It is highly desirable to have the automatic pilot under the control of the human pilot at all times, so that it serves as his assistant to relieve him of the routine operations of flying. He is thus able to devote more attention to other problems, such as engine control and aerial navigation. The advantage of relieving the human pilot in this manner has been emphasized in the larger airplanes where increased personnel have been required despite the additional operations already made automatic. As more of the duties are performed by an "automatic crew" under human guidance, the human crew will perform its work more proficiently and with less fatigue.

There are a number of requirements which an automatic pilot must meet in order to be considered satisfactory. Primarily it must hold any set course and attitude, within the limits of the aircraft performance, for periods suitable to the navigational problems involved. In accomplishing this it must be stable. It must neither cause the control surfaces to flutter nor create hunting of the aircraft of a magnitude that would be noticeable to a passenger. Ease of operation, both in turning the pilot "off" and "on" and in changing course or attitude, is an important factor to the human pilot. Ease of overhaul and repair is equally important to the maintenance crew. As is the case with all equipment for aircraft, light weight and small size are essential, especially in parts mounted on the instrument panel. The automatic pilot should "fail safe," that is, failure of any of its components should not result in violent motion of the airplane. In addition, the automatic pilot must be disconnected easily and overpowered readily.

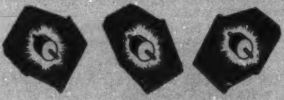

Figure 1. A gyroscope tends to maintain a fixed direction in space

The fundamental operation of an automatic pilot is relatively simple. Basically its operation is analogous to that of a human pilot. When an error in the course or attitude of the plane is detected, both the human pilot and automatic pilot move the control surfaces in such a way as to reduce this error and, so that the resulting motion of the aircraft is stable; that is, correction of course and attitude errors is accomplished smoothly and without overshooting.

Detection of an error in course or attitude requires a reference. Gyroscopes are used almost universally as references for automatic pilots, since they tend to maintain a fixed direction in space, as shown in Figure 1. They also may be used as references by the human pilot when the ground is not visible. Course and attitude errors may be determined by measuring displacement of the airplane with respect to these gyroscopic references. The measuring devices which are known as error detectors or pickoff units correspond to the senses of a human pilot. The signal from the pickoff unit is modified in the proper manner to achieve stability, amplified, and then transmitted to the control surfaces by means of a servomotor. As shown in the appendix, stable operation of the aircraft requires that the motion of the control surface should be responsive to the velocity, and in most cases the acceleration of the airplane in addition to being responsive to the error signal, that is, anticipation of the airplane's future position, is required if stability is to be achieved.[2,3]

Automatic-Pilot-System Components

In conventional automatic-pilot practice the reference units are usually a directional gyroscope, as shown in Figure 2, with its spin axis horizontal for course reference and a horizon gyroscope, as shown in Figure 3, with its spin axis vertical for the pitch and bank references. The directional gyroscope is subject to random drift due to friction present in the unit as well as drift caused by the rotation of the earth. Consequently it is compared normally at periodic intervals with a magnetic compass and reset if necessary. It is possible, however, to couple the directional gyroscope to a magnetic compass so that it is reset continuously and automatically. A magnetic compass is not normally used for a course reference since it is seriously affected by pitching and rolling of the aircraft. However, if a gyroscope with a vertical spin axis is used to maintain the compass in a horizontal plane, it may be used directly as a course reference.

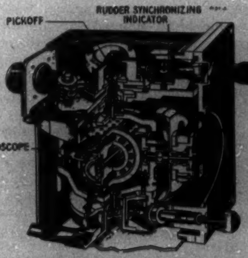

Figure 2. Cutaway view of a directional gyroscopic control instrument

The horizon gyroscope is subject to drift of the same type as that in the directional gyroscope. In this case, however, a pendulous erection system, the average direction of which is the true vertical, is used to correct the instrument.

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The speed of this correction must be sufficiently high to correct for the earth's rotation with a negligible error, but, because of the centrifugal effects on the pendulum during turns, there is an upper limit to the speed at which correction may be applied.

Gyroscopes may be of the air-driven or electrically driven type. The electrically driven units provide better performance at high altitudes, better starting characteristics at low temperatures, and a reduction of the dirt hazard in the instrument. The recent developments of electric gyroscope motors[4] of instrument size has made it practical to mount electrical gyroscopes on the instrument panel, thus utilizing them as flight instruments as well as the references for the automatic pilot. In these small gyroscopes a-c motors of the squirrel-cage or hysteresis type are preferred to d-c motors because of the elimination of brush-wear and radio-interference problems, as well as the ease of attaining higher speeds. The 400-cycle a-c power system which is rapidly being adopted as standard aircraft power makes it possible to obtain high gyroscopic speeds and a resulting high angular momentum of the gyroscope with conventional four- or two-pole motors of the a-c type.

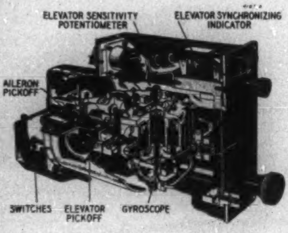
ELEVATOR SENSITIVITY POTENTIOMETER
ELEVATOR SYNCHRONIZING INDICATOR
AILERON PICKOFF
SWITCHES
ELEVATOR PICKOFF
GYROSCOPE

Figure 3. Cutaway view of a bank-and-climb gyroscopic control instrument

The motion of the airplane with respect to these references is normally measured with a pickoff unit which provides a control signal proportional to this motion. The older mechanical or air-valve pickoffs in many cases are being replaced by electrical units which may be classified as the direct-contact or potentiometer type, the variable-inductance or E type, and the induction-regulator or Selsyn type. In general, these units are oriented so as to produce an a-c signal,[5] the polarity and magnitude of which correspond to the direction and amount of deviation of the airplane. Typical output voltage characteristics of the induction type are shown in Figure 4.

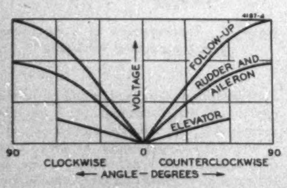

Figure 4. Output voltage characteristic of electrical pickoff and follow-up units of the induction (Selsyn) type

Some of the stabilizing or antihunt signal sources which have been used are as follows:

(*a*). Restrained or rate gyroscopes, if properly oriented, will provide a displacement having a magnitude proportional to the angular velocity of the aircraft about the axis being stabilized.

(*b*). A signal proportional to the position of the control surface or the force on it will contain both acceleration and velocity components of the motion of the aircraft and may be used for stabilization.

(*c*). Accelerometers may be used to obtain acceleration components of the aircraft motion.

(*d*). Various aerodynamic measurements, such as wing-tip velocities, rate of climb, angle of attack, and lift on the wings have been used for stabilizing purposes.

(*e*). The error signal may be operated on directly to obtain derivative components. If this signal is electrical it is possible to perform this operation by means of conventional capacitor–resistance networks.

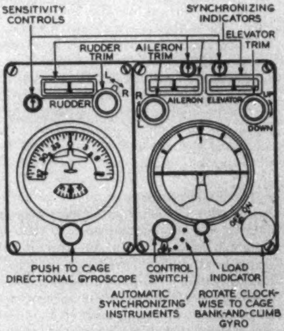
4187-5
SENSITIVITY CONTROLS
SYNCHRONIZING INDICATORS
ELEVATOR TRIM
RUDDER TRIM
AILERON TRIM
RUDDER
L
R
R
L
AILERON
ELEVATOR
UP
DOWN
PUSH TO CAGE DIRECTIONAL GYROSCOPE
CONTROL SWITCH
LOAD INDICATOR
AUTOMATIC SYNCHRONIZING INSTRUMENTS
ROTATE CLOCKWISE TO CAGE BANK-AND-CLIMB GYRO

Figure 5. The control knobs combined on the instrument panel of a relief pilot

The problem of automatic stabilization of an aircraft is complicated somewhat by the fact that the system parameters, in particular the inertia, damping constants, and the response of the aircraft to control surface motion vary with loading, altitude, air speed, and so forth. Although it is true that there is considerable interaction between the various axes of an aircraft, particularly the roll and yaw, it has been found that satisfactory relief pilots can be built without taking account of this interaction.

The power units or servomotors for operating the control cables may be classified according to their power source

Past experience with the pneumatic piston type of servomotor has demonstrated that they are operable, but that it is more difficult to stabilize the aircraft because of the compressibility effects in the servomotor.

The hydraulic piston servomotor eliminates these compressibility effects and provides an easily controlled source of power; low-pressure units similar to those shown in Figure 10 are in common use today. These units generally are provided with a by-pass valve for hydraulic disengagement during manual flight. In order to permit the human pilot to take over control in case of an emergency, relief or overpower valves are placed across the piston.

Electric servomotors are usually high speed, of either the a-c or d-c type, driving through a gear reduction to an output pulley. The use of this type of servomotor has been limited somewhat by the relatively high inertia to torque ratio which introduces a time delay, the weight of the control equipment required to obtain variable speed operation, and the complexity of the mechanical disengagement necessary for manual flight. Motors have been designed specially to reduce the inertia to torque ratio; in other instances the motor has been allowed to run continuously, and the load has been controlled by clutching. Saturable reactors also have been used to control the speed of low-power a-c servomotors. Disengagement is usually accomplished by means of solenoid-operated clutches.

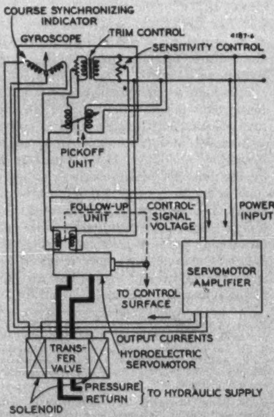
COURSE SYNCHRONIZING INDICATOR
GYROSCOPE
TRIM CONTROL
4187-6
SENSITIVITY CONTROL
PICKOFF UNIT
FOLLOW-UP UNIT
CONTROL-SIGNAL VOLTAGE
POWER INPUT
SERVOMOTOR AMPLIFIER
TO CONTROL SURFACE
OUTPUT CURRENTS
TRANS-FER VALVE
HYDROELECTRIC SERVOMOTOR
PRESSURE
RETURN
TO HYDRAULIC SUPPLY
SOLENOID

Figure 6. Simplified diagram of one control channel of a relief pilot

The type of amplifier units will depend upon the type of pickoff and servomotor used. For a system using an air-valve pickoff and a pneumatic or hydraulic power servomotor, the fluid flow to the cylinder is controlled by a balanced valve which is actuated by a vacuum-operated diaphragm. The differential vacuum on this diaphragm is controlled by the air-valve pickoffs in the gyroscope. At high altitude, because of the reduction of atmospheric pressure, the force available on the valve is reduced, resulting in reduced velocity of the servomotor.

With an electrical pickoff an electronic amplifier is usually used, first, to discriminate the polarity of the incoming signal, and second, to amplify it to a higher-power level. When this amplifier is used to control an electric servomotor, additional control means, such as saturable reactors or solenoid-operated clutches, are used. Hydraulic servomotor units are controlled conveniently by means of a solenoid-operated balanced oil valve.

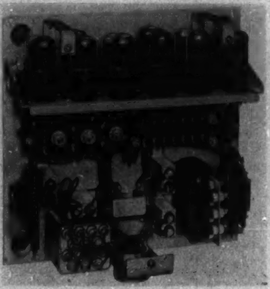

Figure 7. Servomotor amplifier with cover removed

Adjustment of the course and attitude of the airplane during automatic-pilot flight, as well as alignment of the automatic pilot with the airplane before engagement, requires controls readily accessible to the pilot. A change in course or attitude of the airplane is usually accomplished by introducing a trim signal into the control circuit, thereby requiring that the airplane change its attitude to restore equilibrium. This trim signal may be introduced mechanically by displacing the rotor of the pickoff with respect to the stator, by electrically displacing the stator signal by means of a selsyn system, or electrically by introducing a bias voltage. It is also desirable to have control of the damping action of the pilot as well as the control switches readily available. In the maneuvering type of pilot the turn and bank controls are combined, so that motion of one control will introduce a rate of turn with the correct amount of bank.

A Typical Autopilot With Electrical Control

The automatic pilot described in detail in this paper exemplifies the basic features of automatic pilots and also illustrates the trend toward electrification of many of its parts. It is a modern design developed specifically as a relief automatic pilot and intended mainly for straight level flight, although capable of performing simple maneuvers. It incorporates recent developments in small electric gyroscopic flight instruments and has fulfilled the need for an automatic pilot having the advantages of electro-hydraulic operation. Its panel mounting dimensions are interchangeable with those of older designs. A simplified diagram of one channel of the system is shown in Figure 6.

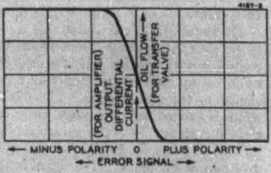

Figure 8. Servomotor amplifier characteristic curves

The gyroscope references contain small hysteresis-type motors running at a synchronous speed of 12,000 rpm from a 115-volt 400-cycle 3-phase supply. The rotor weight running outside the stator provides exceptionally high gyroscopic stabilizing torques. Figure 2 shows this motor mounted in gimbals and geared to a vertical dial for use as a directional flight indicator.

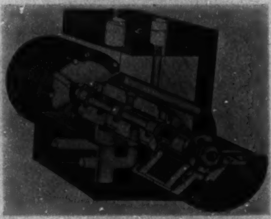

Figure 9. A cutaway view of a solenoid-operated transfer valve

The pickoff units are of the induction type having rotors excited directly from a single phase of the 400-cycle supply and stators wound single phase from which the signals are taken. The rotor of the directional pickoff is mounted on the gyroscopic vertical axis, and the stator of this unit is mounted on the frame. Consequently, there is no mechanical friction imposed on the gyroscope by the pickoff unit, since the rotor excitation is already available on the gimbal for motor power. The stator of the pickoff unit is mounted in a ring gear and driven by the knob on the front panel so as to provide a simple means for changing course while in automatic flight.

Figure 3 shows the arrangement of a similar motor used for the bank and climb control. The spin axis is nearly vertical, and a unique electrical erecting device properly damped[8] and actuated by gravity holds it in this position. During turns the centrifugal force acting on the erecting device may cause a slight drift from its correct position. However, a backward tilt of the gyroscopic spin axis has been introduced to compensate correctly for this effect at a rate of turn of approximately 180 degrees per minute. At other rates of turn the compensation is only approximate.

The aileron pickoff unit is mounted with its primary winding on the main gimbal and its secondary winding on the frame. The elevator pickoff is mounted with its primary winding on the horizon bar linkage, so that it moves with the motor and has its secondary winding on the main gimbal. Typical output voltages from the pickoffs for various angles of deviation are shown in Figure 4.

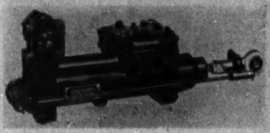

Figure 10. A hydraulic servomotor with an electrical follow-up unit coupled to it

The secondary windings of the aileron and elevator pickoffs are not rotated easily from outside the gyroscope, so that potentiometer trim controls are added with knobs, as shown in Figure 5, and with connections, as shown in Figure 6.

The compactness and light weight of this pilot result in part from mounting all the controls, with the exception of the "on–off" oil valve, directly on the face of the gyroscope. Separate sensitivity control knobs are provided for rudder-, aileron-, and elevator-damping adjustment. Their electrical connections are shown in Figure 6, and the arrangement of the controls is shown in Figure 5. Small synchronizing instruments are provided in the gyroscopic cases to indicate when the trim knobs are correctly set to hold the course and attitude that the plane is flying. These instruments read zero when the currents through the transfer-valve solenoids are balanced.

The electric control switch for the automatic pilot is mounted in the lower left-hand corner of the bank and climb gyroscope. It has positions for:

(*a*). The use of the gyroscopes only as flight instruments.

(*b*). The use of the synchronizing indicators for "trimming" the automatic pilot before engagement.

(*c*). The use of all the components in automatic flight.

In addition to these control knobs, the normal caging and dial adjusting knobs of flight indicating instruments are provided.

The control signals for the rudder, aileron, and elevator functions are fed into three separate amplifier channels in the servomotor amplifier shown in Figure 7. The purpose of the amplifier is to convert the low-level a-c signal input to d-c output of sufficient power to operate the solenoids of the hydraulic transfer valve. Essentially, each channel consists of a balanced two-stage amplifier connected so that the first stage is responsive to the polarity and magnitude of the incoming signal, and the second stage is the power amplifier. With no input signal the output currents of each half of the channel are balanced. With an applied signal the current in one half of the channel will rise, and that in the other half will fall in an amount proportional to the signal voltage; if the signal polarity is reversed the effect on the output current is reversed. The form of the output current versus input signal curve is shown in Figure 8.

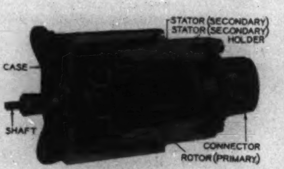

Figure 11. A cutaway view of an electrical follow-up unit

The transfer valve shown in Figure 9 is actuated by the output of the servomotor amplifier, and consists of three hydraulically balanced oil valves, each valve being positioned by proportional solenoids located at each end. These solenoids have armatures suspended in radial air gaps without bearings and are spring biased outwardly in the axial direction from their magnetic centers. Differential excitation of the solenoids produces an axial motion of the armatures which operates the valve. These units have been designed so that the rate of oil flow from the valve is approximately proportional to the differential current through the solenoids. This characteristic, together with the linear electronic amplifier characteristic, results in a servomotor velocity approximately proportional to the input signal up to the saturation limit of the amplifier. The arrangement of solenoids at both ends of each valve insures that the valve will fail safe, that is, return to neutral if the electric signal is removed. Conventional hydraulic servomotor units, of a size dictated by the airplane requirements, are used with this equipment.

The stabilizing signal used in this automatic pilot is a voltage proportional to control surface position. It is generated by follow-up units of the induction type similar to the pickoff units. The rotor, which is excited from the same phase of the 400-cycle supply as the corresponding pickoff, is coupled to the servomotor as shown in Figure 10. The signal from the stator of this unit is connected in series with the pickoff signal before it enters the amplifier. The sinusoidal voltage output characteristic of this unit, as shown in Figure 4, is modified by the nonlinear crank-arm coupling, so as to produce an output voltage essentially proportional to the servomotor piston displacement. A cutaway view of this electrical follow-up unit is shown in Figure 11.

Thus the control-signal voltage entering each amplifier channel is the resultant of three components: the trim control voltage for manually setting the attitude of the plane, the pickoff voltage indicating the deviation of the airplane from the set attitude, and follow-up voltage indicating the deflection of the control surface from its neutral position. The resultant input signal determines the amount and direction of unbalance in the output currents and, consequently, the amount and direction of oil flow from the transfer valve.

The typical sequence of operation of the system may be described in the following manner. Assume that the airplane has been flying in a straight course under the control of the automatic pilot so that the pickoff, follow-up, and trim voltages are zero. Under these conditions the amplifier output currents will be balanced, the transfer valve will be in the neutral position, and the servomotor will be stationary. Now if it is suddenly displaced from its course by an outside force, a signal immediately appears on the gyroscope pickoff thereby disturbing the resultant zero-voltage balance of the amplifier input signal. The resulting plate current unbalance in the power tubes of the amplifier causes the proportional oil valve to move from its neutral position thereby permitting oil flow to the servomotor. The servomotor then moves the control surface in the proper direction to return the airplane to its original course. As soon as the control surface moves, a voltage proportional to the amount of this movement is induced in the stator of the follow-up unit. This voltage opposes the voltage induced in the gyroscope pickoff and so reduces the current unbalance from the power amplifier as well as the displacement of the proportional valve. Consequently, motion of the control surfaces continues until these voltages are equal and opposite. At the same time, however, the displacement of the control surface causes the airplane to return gradually to its original course, thereby gradually reducing the pickoff voltage to zero. This reversal of input signal polarity reverses the oil flow to the servomotor, and so the control surface gradually returns to its neutral position. System equilibrium is again established when the control surface is in the neutral position with zero follow-up voltage and when the airplane is set at the correct attitude with zero gyroscope pickoff voltage.

If the electrical trim control in the case of the elevator or aileron control is moved from its neutral position thereby unbalancing the zero signal input voltage, the new equilibrium point will be reached when the airplane attitude has shifted, so that the gyroscope pickoff voltage is equal and opposite to those introduced by the trim control setting and the new follow-up and control surface position required to maintain this attitude.

If the sensitivity control is adjusted so that the follow-up excitation is decreased, the follow-up signal gradient is reduced, and the control surface must move a greater distance to balance out the pickoff signal. This results in a "stiffer" pilot control.

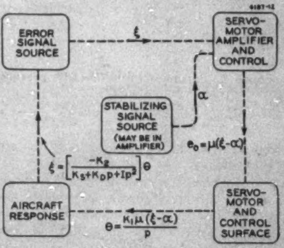
4187-42
ERROR SIGNAL SOURCE
ξ
SERVO-MOTOR AMPLIFIER AND CONTROL
α
STABILIZING SIGNAL SOURCE (MAY BE IN AMPLIFIER)
$e_0 = \mu(\xi - \alpha)$
$\xi = \left[\frac{-K_g}{K_s + K_D p + I p^2}\right]\theta$
AIRCRAFT RESPONSE
$\theta = \frac{K_1 \mu (\xi - \alpha)}{p}$
SERVO-MOTOR AND CONTROL SURFACE

Figure 12. Servomotor system

Samples of this automatic pilot have been used in tests to stabilize airplanes at various altitudes and flying speeds with the indication that the sensitivity control knobs can be set on the ground to a satisfactory position for most normal flying conditions. The stabilization of the airplane as well as the direction held were found to be well within the desirable limits for this type of automatic pilot.

Conclusions

Automatic pilots are one step toward achieving complete automatic controls for aircraft. This step as a separate problem has been solved satisfactorily in a number of ways that are in general use. One of the major advances in the art has been brought about by the electrification of several components for the automatic pilot, including the gyroscope motors, the error signal pickoff, the follow-up units, and the amplifying units including the transfer valve. These have resulted in better starting characteristics, in more satisfactory operation at high altitude, and in the elimination of mechanical linkages to the instrument panel. The electrical pickoff and follow-up signals have made possible electrical trim controls for adjusting the stability of the aircraft. The use of small potentiometers, together with the recently developed electric gyroscopes of instrument size, allow all of the controls with the exception of the "on–off" oil valve to be mounted on the instrument panel directly in front of the human pilot. The automatic pilot described in detail characterizes all the aforementioned features. Flight-test results have proved the superiority of this design as a relief type of automatic pilot.

Appendix. Theory of Automatic-Pilot Operation

The mathematical study of automatically controlled flight is sufficiently complex to warrant an entire paper. Consequently, only a descriptive analysis will be presented to provide a better understanding of the functions of the various components.

Assume that there is no mutual effect among the three controlled axes, and that the problem of stability is concerned only with small angular deviations. One control axis of the pilot then may be represented schematically by the servomotor system shown in Figure 12.

The component parts may be any of the types mentioned in the paper. When an electrical pickoff is used its time constant is practically zero, and its output can be represented by ξ where ξ is proportional to the error angle.

The stabilizing signal source usually has a small time constant, and its output can be represented by α.

The input to the amplifier is $\xi-\alpha$, and if μ is the amplification the output is $\mu(\xi-\alpha)$. Although the time constants of amplifiers are greater than those of the signal devices, they are negligible compared with those of the airplane in the case of electronic amplifiers followed by solenoid-operated transfer valves.

$$e_0=\mu(\xi-\alpha) \qquad (1)$$

This output signal is connected to the servomotor to cause it to move at a rate proportional to the signal. A time constant will appear in the servomotor response, but again this is usually negligible so that if θ is the servomotor and control surface position tending to correct ξ,

$$p\theta=K_1\mu(\xi-\alpha) \qquad (2)$$

where K_1 is the servomotor constant.

The displacement of the control surface, θ, from its neutral point produces an approximately proportional torque about the center of gravity of the airplane, so that the airplane accelerates in a direction to decrease ξ. The resulting angular velocity, however, gives rise to damping and static stability torques which limit the maximum velocity. This is the major time constant in the system and consequently cannot be neglected. The response of the airplane to control surface motion then may be expressed as:

$$K_2\theta=-K_s\xi-K_Dp\xi-Ip^2\xi \qquad (3)$$

where:

K_s = static stability constant
K_2 = control surface constant
K_D = damping constant
I = angular inertia of plane

If equations 2 and 3 are solved simultaneously, there is obtained:

$$\left[p^3+\frac{K_D}{I}p^2+\frac{K_s}{I}p+\frac{K_1K_s}{I}\mu\right]\xi-\frac{K_1K_2\mu}{I}\alpha=0 \qquad (4)$$

With the airplane constants normally encountered in control work and the system gain, $K_1K_2\mu$, required for reasonable accuracy of flight, a stable solution of equation 4 requires that the last term be present in the correct amount. Generally, this term will be of the form:

$$\frac{K_1K_2\mu}{I}\alpha=\frac{K_1K_2\mu}{I}(-Ap-Bp^2)\xi \qquad (5)$$

so that equation 4 becomes:

$$\left[p^3+\left(\frac{K_D}{I}+\frac{BK_1K_2\mu}{I}\right)p^2+\left(\frac{K_s}{I}+\frac{AK_1K_2\mu}{I}\right)p+\frac{K_1K_2\mu}{I}\right]\xi=0 \qquad (6)$$

By a proper choice of the constants B and A the solution of this expression will be damped properly so as to provide stable operation of the airplane. The constants B and A determine, respectively, the magnitudes of the acceleration and velocity components of airplane motion, $p^2\xi$ and $p\xi$, which are used for stabilization. Various means have been used to obtain these components, but perhaps the most convenient and widely used method is to feed back into the amplifier a signal proportional to the position of the control surface. In this case:

$$\alpha=K_3\theta \qquad (7)$$

where K_3 is the feed-back constant.

If equations 7 and 3 are solved simultaneously, there is obtained:

$$\alpha=-\frac{K_3K_s}{K_2}\xi-\frac{K_3K_D}{K_2}p\xi-\frac{K_3I}{K_2}p^2\xi \qquad (8)$$

If equation 8 is substituted in equation 4, the equation of motion is:

$$\left[p^3+\left(\frac{K_D}{I}+K_1K_3\mu\right)p^2+\left(\frac{K_s}{I}+\frac{K_1K_3K_D}{I}\right)p+\frac{K_1K_3K_s\mu}{I}+\frac{K_1K_2\mu}{I}\right]\xi=0 \qquad (9)$$

Proper stability adjustment is now obtained by adjusting K_3. If the control-surface position is obtained as an electrical signal, K_3 then may be varied easily so as to obtain the desired stiffness of control and flight characteristics for various types of airplanes.

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Peak Voltages With D-C Arc Interruption for Aircraft

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DURING the interruption of direct currents in circuits having inductance, the presence of high-voltage peaks or "inductive kicks" is quite commonly recognized in a qualitative sense. There is however little quantitative data on this subject, particularly at the voltages, loads, and altitudes encountered on modern aircraft.

Data on inductive voltages usually are obtained by taking cathode-ray oscillograms or by observing a cathode-ray oscilloscope. The former method is accurate but relatively slow and tedious; the latter, with the tubes available up to this time, has been somewhat questionable because of the difficulty of clearly determining by eye a single fast transient. In this investigation use has been made of a cathode-ray tube with a retentive screen—so retentive that the trace of a single half-cycle sweep of a 60-cycle wave takes several seconds to disappear. Using this tube it has been possible to take a large amount of data in a short time, so that, instead of relying on a few oscillograms, ten readings could be taken at each point and an average and range established for the voltage disturbances set up.

Scope of This Paper

It is the purpose of this paper to present experimental data on the magnitude and variations of voltage peaks set up by d-c arc interruptions of inductive circuits. These voltages were measured on d-c systems of 30, 60, 125, and 250 volts with currents ranging from 0.1 to 220 amperes and simulated altitudes from sea level to 50,000 feet. The loads used included typical aircraft devices such as motor fields and contactor coils.

The effect of the induced-voltage peaks on insulation puncture or flashover was not investigated. This phase of the subject requires a separate investigation.

Because of limits in time, no attempt is made to explain theoretically the results obtained. Rather, emphasis is on the presentation of data taken to date. An investigation was made, at the same time, of the interrupting ability and the arc time of some of the devices used in these tests, and the results are presented in an accompanying paper by Quill and Rader.[1]

Summary

The data presented may be summarized as follows:

1. When a Switchette (the trade name for a small double-break switch with 0.020-inch gaps and opening time of one to two milliseconds) was used on 250 volts and 0.4 ampere at sea level, voltage peaks as high as 28 times line voltage or 7,000 volts were observed.

2. A maximum peak of 130 times line voltage or 3,900 volts was observed while operating the Switchette double-break on 30 volts and 0.4 ampere at sea level.

3. When the Switchette was used the voltage peaks were found to decrease with altitude for all voltages tested. A maximum of seven to one in ratio of voltage at sea level to voltage at 50,000 feet was observed.

4. Using a double-break contactor which has a $^{5}/_{16}$-inch gap per break and a gap-travel time of 12 milliseconds, the induced voltage peaks rise with altitude in every case. A maximum of two to one in the ratio of voltage at 50,000 feet to voltage at sea level was observed. The loads on which these tests were made, as the text of the paper will show, were very much less inductive than those used for the Switchette.

5. Variations of humidity over a wide range has little effect on the voltage peaks at any altitude.

6. There are no voltage peaks above normal line voltage in a purely resistive circuit. For interruptions which take place in circuits having the same system voltage and the same initial current, the more highly inductive circuits caused the higher-voltage peaks.

7. For any particular voltage lower currents in general give higher peaks. An exception was noted with the Switchette where peak voltage increased as current was reduced down to about 0.4 ampere. A further decrease in current gave a reduction in voltage.

8. For the same voltage and current, peak voltages occurring for double break on the Switchette were as much as 190 per cent of the single-break peak.

9. The breakdown potential of one-fourth-inch-diameter brass electrodes with altitude is shown in Figure 3 of the paper, "Potential Breakdown of Small Gaps Under Simulated High-Altitude Conditions."[2] For an air gap of $^{1}/_{32}$ inch the breakdown voltage was 4,000 volts at sea level and 1,000 volts at 50,000 feet altitude. Peak voltages higher than those were found in this investigation, indicating the possibility of damage to insulation due to surface flashover such as might occur in an *AN* connector.

10. Frequencies in the order of 50,000 cycles per second were encountered, usually occurring at low currents and at high altitudes. This is a possible source of interference for radio equipment in the immediate vicinity.

Fundamentals of D-C Interruption

The important factors contributing to the current-interrupting ability of a switch in air are the material of the contacts, the contact configuration, the speed with which the contacts open, the final gap length, the condition of the air, and the constants of the electric circuit itself.

If the circuit contains inductance as well as resistance, as it usually does, interruption will result in high voltages across the contact tips. Consideration of the interruption on the basis of energy reveals two sources of energy, that stored in the inductance and that supplied by the source of power during the interrupting period. The total energy must be dissipated in the resistance of the circuit and in the arc itself. When the tips first part, an arc will be struck, and hence a low arc voltage will appear. Current will decrease as the gap between tips increases until at some low value it suddenly will go to zero or very nearly so. This last sudden change of current causes a high voltage to be induced across the inductance due to $L di/dt$, and this also must appear across the contacts. This voltage may be enough to cause the gap to break down again with a corresponding decrease in voltage to the arc voltage for that particular gap opening. There may be several of these breakdowns, or, on the other hand, the rate of recovery of the dielectric strength of the air may be faster than the first rise in voltage, and hence an interruption will occur on the first attempt. If the current goes to zero, the voltage across the contacts must fall from the peak value to line voltage immediately.

However, indications are that in an appreciable number of cases the current

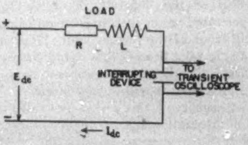

Figure 1. Elementary diagram of test circuit

Paper 44-200, recommended by the AIEE committee on air transportation for presentation at the AIEE Los Angeles technical meeting, Los Angeles, Calif., August 29–September 1, 1944. Manuscript submitted June 14, 1944; made available for printing July 12, 1944.

VIRGEL E. PHILLIPS and WALTER P. MITCHEL, both now in the Armed Forces, were employed in the industrial-control engineering division of General Electric Company, Schenectady, N. Y., at the time this paper was written.

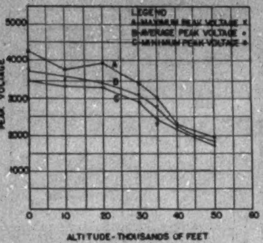

Figure 2. Peak-voltage distribution versus altitude resulting from interruption of an inductive load of 0.4 ampere at 30 volts direct current with the double-break Switchette

Gap: 0.020 inch per break. Ten consecutive readings were taken, and the average, maximum, and minimum readings are shown. Load: inductive *A*, Table I, 0.4 ampere—four coils in parallel

drops suddenly from normal arc current to a very low current which may continue for some time. Thus voltage across the contacts in this case would not fall rapidly from the peak value down to line voltage, but rather only to some intermediate value, and then slowly approach line voltage. This phenomenon is similar to that of the normal and abnormal glow periods as described by Slepian.[3] Also, the glow period following the peak has been noted by the authors in the experimental tests. One common form was for the voltage to drop from the peak down to 300–500 volts and remain for periods of the order of one millisecond, and then taper off to line voltage.

Although the initial conditions of voltage and current are the same before each opening, arc characteristics and peak voltage may differ widely on successive interruptions. Some causes for these variations may be: variations in opening speed, variations in air conditions and the amount of convection action on the arc, and irregularities in the contact surface causing the arc to strike at different points on the contact.

Gap opening except in a limited way and opening speed are factors in the peak voltage reached, but this investigation did not cover these variables. In general, to prevent excessively high peak voltages, a d-c contactor that opens rapidly should not open too wide.[4]

Test Equipment and Procedure

In the measurement of the peak voltages, use was made of a new transient oscilloscope and associated circuit developed in the industrial-control engineering division of the General Electric Company. Its main feature is a high-voltage electronic tube, similar in shape and size to that used in the cathode-ray oscillograph, but having a highly persistent fluorescent screen which allows it to retain the trace of a single transient long enough to be observed by the eye. Thus peaks are read quite easily and accurately, and the wave shape on each interruption can be observed. A resistance–capacitance voltage divider designed to operate independently of the frequency over the range of frequencies involved was used. Every effort was made to keep the leads short and the relative location of the equipment such as to minimize any pickup caused by the high-frequency oscillations at interruption.

The general form of the test circuit is shown in Figure 1. The circuit-interrupting device was placed in a sealed bell jar which could be evacuated. Leads were brought out through sealed terminals at the bottom. The effect of altitude was simulated by reducing the pressure within the bell jar, no attempt being made to control either humidity or temperature. The conversion of pressure to elevation was based on data from National Advisory Committee of Aeronautics report 218.

For the high-humidity test a container of water with an electric-heater attachment was placed within the bell jar and left until water was condensing out around the bottom and on the jar itself. Thus a humidity approaching 100 per cent was attained and held for the duration of this run.

All the data presented in this paper were taken on the following two devices:

1. An aircraft switch known as the Switchette. This is a small double-break switch rated at ten amperes, single circuit, 30 volts, inductive load at all altitudes. It has silver tips of one-eighth-inch diameter and a gap of 0.020 inch. Single-break data were taken using this same Switchette with one contact blocked closed. Hereafter this device will be designated as Switchette single-break or Switchette double-break.

2. A double-break switch with a bridge-type movable contact arm carrying silver tips about one-half-inch diameter on seven-eighths-inch centers. The tip gap was set at 0.312 inch. This unit was a part of a commercial size-1 contactor (*CR*2810–1811) having a tip rating on alternating current of 25 amperes and a motor rating of $7^1/_2$ horsepower at 550 volts. Hereafter this device will be designated as the double-break contactor.

The loads used are listed in Table I. The inductances given are differential inductances[5] calculated from magnetic oscillograms of current build-up. They serve the purpose of providing a basis of comparison of inductances and hence some indication of the severity of the peak voltages. However, these values should not be accepted as those to use in calculations, as inductance is not a constant but rather a function of the current and rate of change of current for iron-core coils. For example, inductive *A* shows 55 henrys at 50 per cent current and 8 henrys at 100 per cent current. Also

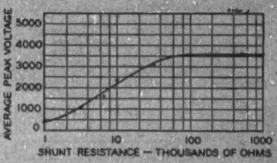

Figure 3. Average peak voltage versus resistance shunted across contacts resulting from the interruption of an inductive load of 0.4 ampere at 30 volts direct current with a double-break Switchette at sea level

Gap: 0.020 inch per break. The average is taken for ten consecutive readings. Load: inductive *A*, Table I, 0.4 ampere—four coils in parallel

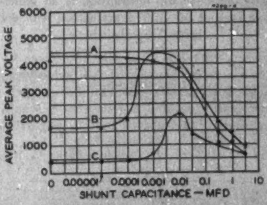

Figure 4. Average peak voltage versus capacitance shunted across contacts resulting from the interruption of an inductive load of 0.2 and 0.4 ampere at 125 volts with a single-break Switchette

Gap: 0.020 inch. The average is taken for ten consecutive readings. Load: inductive *A*, Table I, 0.2 ampere—two coils in series, 0.4 ampere—one coil

A—0.2 ampere—0 feet •

B—0.4 ampere—0 feet ×

C—0.2 ampere—50,000 feet •

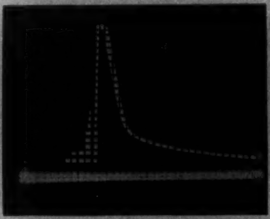

Figure 5. Transient oscilloscope's screen during interruption of an inductive load of 0.2 ampere at 30 volts direct current with the double-break Switchette at sea level

Gap: 0.020 inch per break. Calibration—123 volts per division vertical—one-fourth millisecond per division horizontal. Load: inductive *A*, Table I, 0.2 ampere—two coils in parallel

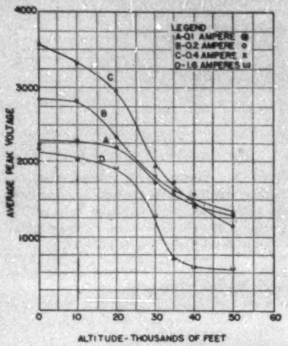

Figure 6. Average peak voltage versus altitude resulting from interruption of an inductive load at 30 volts direct current with the single-break Switchette

Gap: 0.020 inch. The average is taken for ten consecutive readings. Load: inductive *A*, Table I, 0.1 ampere—one coil, 0.2 ampere—two coils in parallel, 0.4 ampere—four coils in parallel, 1.6 ampere—16 coils in parallel

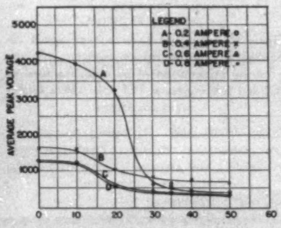

Figure 7. Average peak voltage versus altitude resulting from interruption of an inductive load at 125 volts direct current with the single-break Switchette

Gap: 0.020 inch. The average is taken from ten consecutive readings. Load: inductive *A*, Table I, 0.2 ampere—two coils in series. Parallel combinations of above were used for 0.4, 0.6, and 0.8 ampere

it must be recognized that the inductances found by this method are not the exact values governing current interruption, as the more nearly correct figures would be taken on current decay.

Inductive-*C* load was used for all tests made on the double-break contactor. Current was varied over the range desired by changing a series resistance. Inductive *A* was used for most of the Switchette data. The values shown are for one coil, the desired current at any voltage being obtained by changing the series-parallel combination of these single coils.

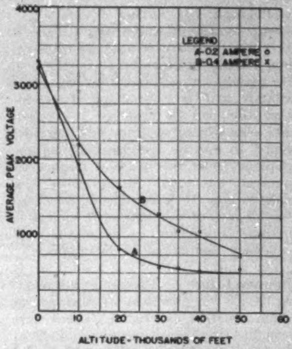

Figure 8. Average peak voltage versus altitude resulting from interruption of an inductive load at 125 volts direct current with the single-break Switchette

Gap: 0.020 inch. The average is taken for ten consecutive readings. Load: inductive *A*, Table I, 0.4 ampere—two coils in series, 0.2 ampere—two coils in series plus external resistance

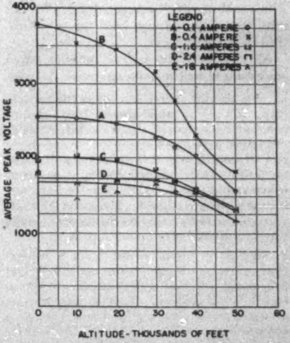

Figure 9. Average peak voltage versus altitude resulting from interruption of an inductive load at 30 volts direct current with the double-break Switchette

Gap: 0.020 inch per break. The average was taken for ten consecutive readings. Load: inductive *A*, Table I, 0.1 ampere—one coil. Parallel combinations of above were used for 0.4, 1.6, and 2.4 amperes. A coil with one-ohm resistance, 360 turns, and 8.5 square inches mean cross-sectional area was used for 18 amperes

As typical aircraft applications inductive-*H*, *J*, and *K* loads were tested. Also used was the shunt field of a one-ninth horsepower aircraft motor, but no inductance figures are given for it.

Data taken previously indicated that peak voltages were higher for a load of a coil with an iron core than for the same coil with an air core. However, peak voltages were much more erratic with the air-core coil, varying as much as two to one on consecutive openings.

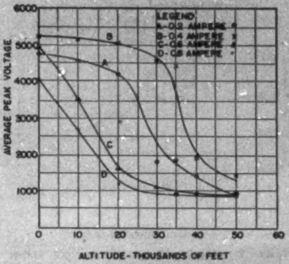

Figure 10. Average peak voltage versus altitude resulting from interruption of an inductive load at 125 volts direct current with the double-break Switchette

Gap: 0.020 inch per break. The average was taken for ten consecutive readings. Load: inductive *A*, Table I, 0.2 ampere—two coils in series. Parallel combinations of above were used for 0.4, 0.6, and 0.8 ampere

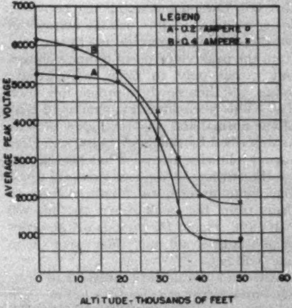

Figure 11. Average peak voltage versus altitude resulting from interruption of an inductive load at 250 volts direct current with the double-break Switchette

Gap: 0.020 inch per break. The average was taken for ten consecutive readings. Load: inductive *A*, Table I, 0.4 ampere—two coils in series, 0.2 ampere—two coils in series plus external resistance

Method of Test

The trace on the screen of the transient oscilloscope at each opening of the interrupting device represents the varia-

Table I. Load Inductance

Load Designation	Load	Load Data	Load Rating: 100 Per Cent V	Load Rating: 100 Per Cent I	Inductance—Henrys at 50 Per Cent I	Inductance—Henrys at 100 Per Cent I
Inductive *A*	Series and series-parallel combinations of d-c industrial relays— data given from each relay	9,600 turns 1.5-square-inch iron area Armature sealed	125	0.42	55	8
Inductive *C*	D-c series motor field *MDO*-109 150 horsepower	Series field and commutating field	220	480	0.002	0.0044
Inductive *H*	100-ampere dynamotor contactor coil	2,300 turns 0.5-square-inch iron area Armature sealed	28	0.54	2.4	1.5
Inductive *J*	Solenoid-type 200-ampere contactor coil		24	0.42	4.2	1.0
Inductive *K*	Balanced armature-type 100-ampere contactor coil	3,100 turns 0.09-square-inch iron area Armature sealed	24	0.19		

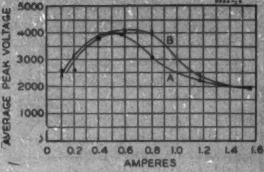

Figure 14. Effect of humidity on peak voltage resulting from interruption of an inductive load at 30 volts direct current with the double-break Switchette at sea level

Load: inductive *A*, Table I
A—Room humidity •
B—High humidity ×

tion of voltage across the contacts with time. In taking readings, the contacts were closed long enough to allow the current to reach its steady-state value, and then the maximum voltage across the contacts as they opened was recorded. In all the data presented the peak voltages are the average of ten values taken on ten consecutive interruptions. A typical distribution of these ten readings is shown in Figure 2. The average values have the advantage of giving a more uniform curve. Also, any insulation breakdown due to peak voltages which causes surface flashover and tracking probably will be a gradual result of a large number of high voltages which can be represented best by the average value. Repeat tests taken at a later date indicate that test points can be reproduced within ten per cent.

Effect of Parallel Resistance or Capacitance

An investigation was made of the effect on voltage peaks of resistance or capacitance shunted across the contacts. In Figure 3 is plotted the variation of peak voltage with shunt resistance over a range from 1,000 ohms to one megohm. Above 100,000 ohms, the peaks remained unchanged by an increase in resistance. As the input resistance to the transient oscilloscope is two megohms, it is well above the range at which appreciable suppressing effect is present. This fact is significant when it is recalled that, if a magnetic oscillograph is used to measure arc voltages, resistance must be placed in parallel with the tips of the interrupting device. The magnitude of this shunt resistance is proportional to the system voltage and exerts an appreciable suppressing effect on the peak voltage. Thus a magnetic oscillograph is usually unsatisfactory for measuring peak voltages, even if the frequency response were otherwise satisfactory.

In Figure 4, average peak voltage is plotted against capacitance shunted

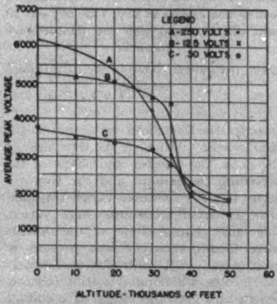

Figure 12. Average peak voltage (ten readings) versus altitude resulting from interruption of an inductive load of 0.4 ampere at 250, 125, and 30 volts direct current with the double-break Switchette

Load: inductive *A*, Table I: gap: 0.020 inch per break

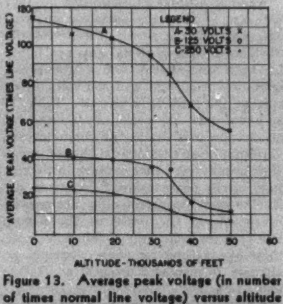

Figure 13. Average peak voltage (in number of times normal line voltage) versus altitude resulting from interruption of an inductive load of 0.4 ampere at 250, 125, and 30 volts direct current with the double-break Switchette

Load: inductive *A*, Table I, gap: 0.020 inch per break

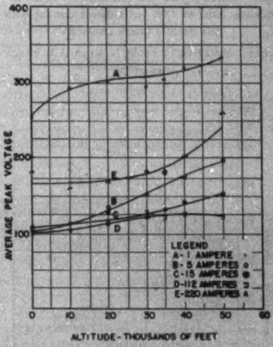

Figure 15. Average peak voltage versus altitude resulting from the interruption of an inductive load at 30 volts direct current with the double-break contactor

Load: inductive C, Table I, gap: 0.312 inch per break

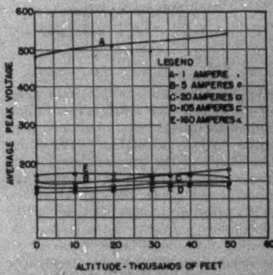

Figure 16. Average peak voltage versus altitude resulting from the interruption of an inductive load at 60 volts direct current with the double-break contactor

Load: inductive C, Table I, gap: 0.312 inch per break

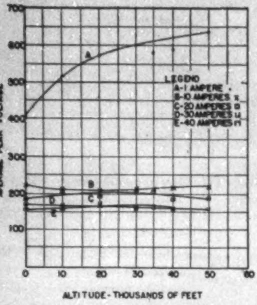

Figure 17. Average peak voltage versus altitude resulting from the interruption of an inductive load at 125 volts direct current with the double-break contactor

Load: inductive C, Table I; gap: 0.312 inch per break

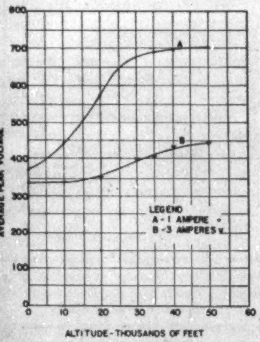

Figure 18. Average peak voltage versus altitude resulting from the interruption of an inductive load at 250 volts direct current with the double-break contactor

Load: inductive C, Table I; gap 0.312 inch per break

across the contacts on an improvised semilog scale. The voltages remain uniform with increase in capacitance until about 0.0001 microfarad. The input capacitance of the transient oscilloscope is approximately 0.00003 microfarad, and hence is well below the critical values. It is interesting to note that two of the curves show a definite rise in voltage, indicating that, while large values of capacitance are effective in suppressing peak voltages, certain critical values may give rise to abnormally high peaks.

Test Results

Wave Shape of Typical Interruptions. In Figure 5 is shown a typical recovery trace for the Switchette as viewed on the oscilloscope. Although the sharpness of the peak, the number and shape of the preceding oscillations, and the time for decay to circuit voltage varied considerably as circuit constants were varied, this same general shape of the voltage wave during recovery was present in most cases. In a few traces, particularly at 30 volts, a horizontal portion (characteristic of a glow discharge) at a voltage several times line voltage appeared sometimes before and sometimes after the peak. In general the peaks occurring with the double-break contactor appeared much sharper than those with the Switchette, and build-up and decay periods were longer and had less oscillatory disturbance.

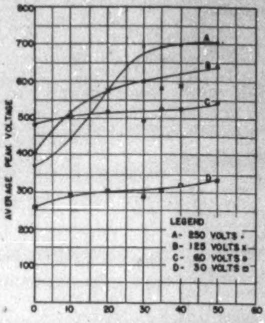

Figure 19. Average peak voltage versus altitude resulting from interruption of an inductive load of one ampere at 250, 125, 60, and 30 volts direct current with the double-break contactor

Load: inductive C, Table I; gap: 0.312 inch per break

Effect of Altitude on Switchette. Figures 6 to 11 show the variation of average peak voltage versus altitude for the Switchette with a 20-mil gap per break for both single-break and double-break operation at 30, 125, and 250 volts direct current on load inductive *A*, Table I. In every case, the peaks are lowered by an increase in altitude, the characteristic variation being that of an S-shaped curve with usually very little change in magnitude of the peaks taking place near sea level or near 50,000 feet. The decrease in magnitude of the peaks at the higher line voltages represents a change of more than five to one for some currents, as altitude is increased from 0 to 50,000 feet.

Effect of Current on Switchette. It should be noted that in most cases as current is increased from a very low value up to 0.4 or 0.6 ampere, the magnitude of the peak increases, after which an increase in current lowers the peaks.

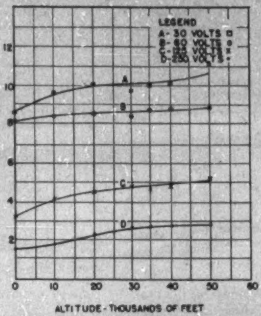

Figure 20. Average peak voltage (in number of times normal line voltage) versus altitude resulting from interruption of an inductive load of one ampere at 250, 125, 60, and 30 volts direct current with the double-break contactor

Load: inductive C, Table I; gap: 0.312 inch per break

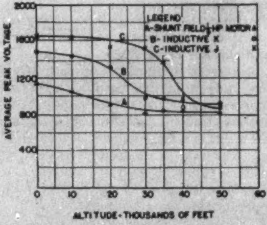

Figure 21. Average peak voltage versus altitude resulting from interruption of inductive loads at 30 volts direct current with the double-break Switchette

Load: aircraft devices now found on 30-volt systems. See Table I

There is some reason, based on tests not reported here, to believe that the magnitude of the peaks again increases as the current approaches some considerably higher value, providing of course that the interrupting limit is not exceeded. This variation of the peaks with current is much more noticeable at sea level than at high altitude.

Effect of Line Voltage on Switchette. In order to show the comparative peak voltages attained with the three different applied voltages at the same current, Figure 12 is plotted. This indicates the comparative severity of the peaks under one of the worst conditions—that is, 0.4 ampere, double-break. The same effect is shown perhaps more spectacularly by Figure 13 in which the ordinates have been changed from absolute voltage to number of times line voltage. In making this comparison at different line voltages, it should be noticed that the inductance values are different. The inductances at 30 and 125 volts are about the same, whereas that at 250 volts is considerably lower.

Effect of Humidity. In order to show the effect of humidity on peak voltages, a run was taken at 30 volts, zero altitude and the results plotted in Figure 14, showing peak voltages as a function of current, both at room humidity and a very high relative humidity (near 100 per cent). According to these results, a wide variation in humidity does not affect materially the magnitude of the peaks.

Effect of Altitude on Double-Break Contactor. To cover higher-current ranges, Figures 15 to 18 are presented for the double-break contactor. It will be seen that the general tendency is for peak voltages to stay fairly constant or to increase somewhat with altitude, depending upon the current interrupted. Noteworthy here are the curves for the 60-volt and 125-volt supplies where the peaks at one ampere jump to two or three times those at the higher currents. In general, in the current range used here the peaks decrease with increasing current, though this decrease is small at the higher currents. There is, however, some indication again of an increase in the peaks at some fairly high current (Figures 15 and 16).

The peak voltages versus altitude resulting from the interruption of one ampere at the four voltages are shown on Figures 19 and 20.

Typical Aircraft Loads. Figure 21 shows the peaks resulting from interruption of a 30-volt circuit by the Switchette (double-break) using inductive loads *J* and *K* and the shunt field of a one-ninth-horsepower motor, all of which are typical aircraft devices. In the first two cases, the current was something above one-half ampere; and in the latter case, about one-quarter ampere. It must be realized, however, that the inductances dealt with here were much smaller, than those of load inductive *A*. Inductive load *H* was also tested and gave very nearly the same peaks as *K*. These peaks must not be construed to be the maximum occurring with any aircraft device, but were taken as being for typical applications. Other devices, possibly with critical values of lead capacitance, might give rise to higher values than for the cases shown.

Conclusions

Experimental data presented in this paper show that voltages of 7,000 volts are possible across the contacts of a switch-interrupting direct current. While the breakdown effect of these peak voltages has not been studied, it is quite possible that they are sufficiently high to cause insulation breakdown or tracking. It was also shown that these peaks are not always worse at altitude.

The two devices investigated gave considerably different results, but this is not surprising, for the devices were not similar. Furthermore, the load ranges and values of inductance differed widely. The subject requires a considerable amount of further investigation.

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Very High-Frequency Radio-Noise Elimination

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THE subject of radio-noise elimination on aircraft has always been a controversial one, even when the frequencies involved were relatively low. With the introduction of very high-frequency equipment the controversy has started afresh. The presence of radio noise in very high-frequency installations has caused observers to forget the principles learned in the work at medium frequency and high frequency, simply because the very high-frequency installation apparently did not follow the same rules that applied to the lower-frequency equipment. It is the purpose of this paper to outline generally the principles of radio-noise elimination at all frequencies, and show how these same principles apply specifically at very high frequency.

Because of the variety of conditions which may be encountered in aircraft radio-noise tests, it will not be possible to detail the test procedures and corrective measures necessary for very high-frequency radio-noise elimination. Rather, it is desired to discuss basic concepts in such a manner that radio-noise test procedure can be made a matter of routine. An outline of test procedure insofar as the engine ignition system is concerned is given, and the remedial measures necessary are generally indicated. The whole procedure is based upon the concept of determining the coupling path into the receiver, with the idea in mind that once this is known the method of eliminating the coupling path is best reached by a consideration of the construction of the airplane under test.

It is unfortunate that many of the terms in use in radio-noise-elimination work are so broad and ill-defined that they cannot be used without careful definition. Since the loose use of these terms has led to some confusion, immediately following a list of definitions is given.

Definitions

A *radio-influence source* is defined as a generator of radio-influence voltages, currents, or fields, and which is capable of exciting a radio receiver in such a way as to produce acoustic output, providing the necessary coupling exists between the radio-influence source and the radio receiver. This implies that the radio-influence source is only influential in providing such an output, and that the measurement of the source is not a measure of the final result.

Radio noise refers specifically to that portion of the acoustic output of a radio receiver produced by coupling a radio-influence source in any manner whatsoever to the radio receiver.

A *coupling source* or *path* is defined as the impedance or circuit element which is excited by the radio-influence source so as to produce acoustic noise in the output of the radio receiver. The coupling source or path may be the radio-influence source in some cases, but may be any part of a circuit, such as a wire, in which radio-influence currents are flowing.

Ground plane—structure ground. Although separated from earth, the metal body of the aircraft acts as a reference plane for the purpose of this paper, and all potentials are in reference to this point. The plane of ground potential is not an obscure hypothetical plane diffi-

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cult to locate as in many earth transmission problems, but is a sharply defined plane of nearly uniform potential existing directly at any metal portion of the structure.

Antenna refers only to the portion of the antenna circuit outside the fuselage, and specifically excepts any portion which is inside the fuselage.

Leadin refers only to the portion of the antenna circuit which is either inside the fuselage or is shielded in any way to prevent undesired coupling of any type.

The *antenna circuit* of a radio receiver is defined as that circuit through which the current flows as a result of signal voltages generated in the antenna due to its presence in a radiation field from a transmitter antenna. As such, it includes the antenna, the leadin (either shielded or unshielded), the receiver input circuit, and the ground-return circuit.

Methods of Radio-Noise Generation

The radio-influence generators with which we are most concerned are those within the airplane itself, and are almost exclusively electrical devices of one sort or another. At medium frequency and high frequency almost any electrical device may act as a generator of radio influence, but at very high frequency the main source is the engine ignition system. Other devices may be troublesome, but these cases are infrequent. They will be dealt with later in this paper.

THE AIRCRAFT IGNITION SYSTEM

The aircraft ignition system consists essentially of a high-voltage magneto and distributor. See Figure 1 for a sketch of the system.

In the magneto itself, the primary circuit is controlled by the opening and closing of a contact switch. The secondary circuit is connected through the distributor to the appropriate spark plug. Provisions are made for starting by the use of either a booster coil or an ignition vibrator; the booster coil feeds high voltage directly into the distributor, while the ignition-vibrator type is directly connected to the magneto primary coil. In either case, operating power is supplied by the d-c power system of the airplane.

In order that the pilot may control the ignition system, a wire from the primary side of the magneto is run to a control switch in the cockpit. Here, provisions are made for grounding the primary of the magneto. The very steep fronts characterizing the voltage in the primary circuit (see Figure 1A) make it capable of impulse exciting any circuit to which it is coupled. For this reason the magneto grounding switch and the grounding leads should be well shielded. The switch should be one in which the battery switch is not included, and should be grounded to basic structure as close to the switch as possible. If the grounding wire runs outside the ignition switch box, it should be shielded also.

Since the voltage in the secondary circuit is definitely of oscillatory character (see Figure 1B), it is necessary to shield every portion of this circuit very thoroughly. The instantaneous energy content of these oscillations is very high, and the frequency range goes up into the superhigh-frequency region.

The oscillatory circuit, composed in the main of the spark plug, the spark-plug-lead capacitance and inductance, is not a simple circuit. There may be several oscillatory circuits set up, each with its own fundamental frequency. The lowest-frequency circuit will be that one utilizing the full-lead inductance and capacitance; peak currents here may go as high as 85 amperes.[1] A higher-frequency oscillatory circuit may be one formed by the spark plug itself, and peak currents here may run from 100 to 175 amperes.[2] Furthermore, there may be a local oscillatory circuit formed in the magneto and distributor housing, with peak currents here also running into high magnitudes.

It must be appreciated that the shielding of circuits containing currents of such high magnitudes is not an easy matter. The design of all mating parts must be such as to reduce the impedance to as low a value as possible; simple butt joints, pressure-held, are unsatisfactory, as they rarely permit continuous contact surface. In a conduit coupling joint, for example, it is not enough that contact be made at one or two points on the circumference of the fitting. There must be a number of contacts about the circumference, so that the radio-frequency current may have a flow path at any point, and the current density about the joint is evenly distributed.

Since the booster coil is connected directly into the distributor, and is operated by the airplane's electric system, it provides a simple method whereby the radio-frequency energy generated in the high-voltage system may be coupled into the aircraft wiring system. The ignition vibrator, on the other hand, connects to the low-voltage side. This is also a coupling path to the aircraft wiring system, but is not so effective as the one through the booster coil. In either case it is wise to place a high-frequency filter in the positive feed to these devices.

Up to now the magneto grounding leads have been treated as if they carried only the pulses which are characteristic of the low-voltage side of the ignition system. However, since these leads enter the magneto, it is inevitable that they should be affected to some extent by the radio-frequency fields set up by the secondary or high-voltage system. They do carry small radio-frequency currents from this source, but generally the amplitude of these currents is small and not nearly so effective in causing radio noise as the pulses set up by the action of the contacts.

The aircraft ignition system, therefore, may be divided into two parts as far as its radio-influence generating capacities are concerned. First, there is the low voltage or primary side of the magneto; the only coupling path for this system is through the magneto grounding leads and the ignition vibrator. Second, there is the high-voltage side or secondary side, which may radiate directly, or may couple through the booster coil. These coupling paths are the only ones by which the engine ignition system may transfer radio-frequency energy outside itself. This last statement must be considered carefully by those making radio-noise investigations. If all these coupling paths are controlled, there is no chance of radio-noise troubles. Radio-noise investigations involving the ignition system are simply problems of determining the coupling path and eliminating it. The high-voltage system may be the most difficult to control, as such high peak energies are involved, and the return paths for the radio-frequency currents are often difficult to control. Magneto and distributor construction is such as to provide possible high resistances in the current return path, such as the mating surfaces between magneto and distributor parts. However, the currents here are low in value compared with those in the spark-plug leads (except where local oscillatory circuits are formed), and are controlled much more easily. This is almost entirely a matter of good magneto and distributor design.

OTHER RADIO-NOISE SOURCES

As previously noted, other radio-noise sources are very few in number, particularly at very high frequency. What few there are depend upon their ability to produce a voltage surge with a steep wave-front, so almost any interrupting device may be effective. Cases have been encountered where small motors have caused trouble, even though no commutator trouble was evident. Here the motor was high speed, with consequent high-frequency commutator ripple, and was mounted upon a plywood deck where bonding was difficult. Good bonding was the answer in all cases.

Ignition systems for heaters have been developed in the past few years which are a modification of the aircraft-engine ignition system. That is, either a spark coil controlled by an interrupter and fed from the d-c power system may supply a conventional spark plug, or a transformer operating off the 400-cycle supply may operate the spark plug. In either case, a great deal of interference may result. The problems presented by this system are identical to those presented by the engine ignition system, with the added complication that the equipment is located within the fuselage. It is necessary to shield the secondary circuit very thoroughly, whereas the primary circuit must be well filtered.

As is well known, radio-influence sources are plentiful at medium frequency and high frequency, and are usually motors of one sort or another; dynamotors are apt to be particularly troublesome. In any event, none of the devices will cause trouble if the coupling paths once are determined and eliminated. A small amount of induction coupling is evident from some of these devices on occasion, particularly if they are unbonded. If the leadins are run clear of the motors no trouble will be experienced in any but the exceptional case. The main coupling path is through the aircraft wiring, and this will be dealt with later.

The aforementioned references to bonding must not be construed to mean that bonding connectors are to be used; it is only necessary that the motor or dynamotor case be well grounded through the usual bolts or clips. Where the part is mounted upon a wooden deck it is usual to provide good bonding to the nearest structure.

Methods of Radio-Noise Transmission

The output of a radio-influence generator is of no consequence if a coupling path does not exist between the generator and some impedance element in the receiver input circuit. For some time it was believed that either the antenna or the antenna leadin was always the impedance element affected and that radiation was the coupling path, but this has been disproved largely at medium and high frequency. At these frequencies, the antenna is almost never affected by radio-influence sources within the airplane. The coupling path is almost invariably the aircraft wiring system, either by coupling to the leadin or common impedance coupling which will be discussed later.

At very high frequency the situation is somewhat different; direct coupling may occur to the engine ignition system, and the antenna will be the impedance element affected. There are, however, other coupling paths at these frequencies, and it is the purpose of this section to discuss these paths.

Before proceeding with the discussion it must be pointed out that the conventional methods of measuring radio-influence sources are in error, in that only the voltage is measured. The voltage measured is a function of the internal impedance of the source, the impedance of the external circuit to which it is connected, and the open-circuit voltage of the source, and consequently will change rapidly with any change in the output-circuit impedance. Resonance effects also enter, as the aircraft wiring system must be considered as an impedance network. It is therefore possible to have high voltage and low current at the radio-influence source output, and low voltage and high current at some point in the network, and vice versa. In any event, since either electromagnetic, electrostatic, radiation, or any combination of these couplings may occur, and both the current and the voltage at the impedance element through which coupling occurs must be taken into account, voltage measurements alone are insufficient.

The Aircraft Wiring System—Unshielded

As might be expected, the aircraft wiring system exhibits transmission-line characteristics. To determine the attenuation that might be encountered in a section of aircraft wiring, a series of tests was made on a typical aircraft open wiring bundle consisting of 15 wires. The results of this test are shown in Figure 2, where the attenuation of the aircraft wiring is compared with the attenuation of conventional coaxial cables. For the aircraft wiring the measurements were made between one wire and structure ground. It will be noted that, while the attenuation of aircraft wiring is considerably higher than for a good twin-conductor coaxial cable, it was only slightly higher than for a rubber-insulated twisted pair. The curves shown would be entirely valid if aircraft wiring consisted of simple runs from one point to another, but it does not. The aircraft wiring bundle may contain a number of cables, terminating in lights, switches, or other devices, and an analysis of the aircraft wiring system on the basis of a transmission line would not be very useful. It is preferable to consider it on the basis of an impedance network, as previously noted. On that basis the voltage and current relationships in any network element are a function of the impedance, the voltage and current, and the mutual inductance relationships of all the other elements, plus the generator voltage and internal impedance. Even an approximate calculation of the relationships would be very complex, this consideration being presented simply to indicate the type of approach necessary. An approach by means of voltage measurements alone is inherently faulty.

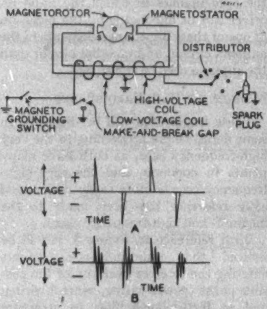

Figure 1. Aircraft ignition system

Magnetorotor, distributor, and make-and-break gap. All are engine-driven and set to produce spark at proper moment in engine cycle

A. Voltage on magneto grounding wire, peak about 200 volts

B. Voltage in secondary circuit, peak depending on pressure, gap spacing, and so forth—about 15,000 volts

It is evident that aircraft open wiring of itself cannot be depended upon for any large attenuation, except in rather long runs. It is also evident that aircraft open wiring will act precisely like any other transmission line used to couple a generator to a terminal impedance. If there is a mismatch, reflections will occur with the formation of standing waves upon the line. Since one or more wires in a bundle may be carrying a wave from the same radio-influence source, but fed over a slightly different path, the standing waves on one wire will not agree in location with the standing waves on other wires. The result of this will be that any attempt to locate standing waves with a noisemeter will be quite confusing, especially at the higher frequencies.

It is probable that this has contributed to the many attempts at source suppression; the whole fuselage area seems full of "noise" and source filtering does eliminate it. The factors which make source filtering impractical have been reviewed in other literature, however, and will not be taken up here.[3]

The Aircraft Wiring System—Shielded

A discussion of the portion of the aircraft wiring that is still shielded may seem academic, as there is so little of it left. Nevertheless, the portions that remain shielded are quite important and deserve attention. In the main, these portions are the ignition system, particularly the magneto grounding leads.

The shielded wiring also may be considered as a transmission line. The attenuation here is somewhat greater than for open wiring, since a great portion of the area of the conduit is filled with a dielectric of relatively high loss at high frequencies. Current flows on the conductors inside the conduit and upon the inner surface of the conduit exactly as in a coaxial cable, the resultant field being confined within the conduit for all practical purposes.

A conduit, either solid or flexible, may contain a joint at some place in the line, and such joints easily may be of rather high resistance, if the joint is not well made. If this local area of high impedance is located where the current density is high, the current will seek a better path, in many instances flowing entirely outside the conduit. This area of high current density outside the conduit will create a local field and may excite conduit which is some appropriate multiple of one-quarter wave. If this field is set up in a section that is not well shielded or near one of the openings in the fuselage, radiation may take place outside the fuselage, and the antenna will be affected directly.

It must be emphasized here that the field produced by current flow outside the conduit is not the same field that is found within the conduit and which is created by interaction between the current flowing on the conductors and on the inner surface of the conduit. That is, it must not be conceived that the field within the conduit penetrates through the conduit in some manner; nothing of the sort occurs. A better conception is that the field within the conduit is the power-transmission medium, and the currents that flow are set up by the field and are a source of losses. When these currents flow outside the conduit a local field is created there, incidentally introducing another loss.

Radiation is most likely to take place from the engine-section ignition system, which in most cases is enclosed in flexible conduit. The leads to the spark plugs are very high-frequency oscillatory circuits, as previously noted, and the current density at joints or glands may be extremely high. A poor joint in this area cannot be tolerated; the field set up about the joint is of such intensity that the engine cowling is insufficient to attenuate it, and direct coupling to the antenna results.

Flexible conduit is often suspected of radiation, since it is almost invariably possible to detect a small field about it. This is the result of the in-and-out weave of the outer sheath, a small portion of the current flowing upon the inner surface of the conduit being conducted to the outside surface, setting up a small field there. The field noticed is the resultant of all the fields set up by the tiny currents flowing outside the conduit.

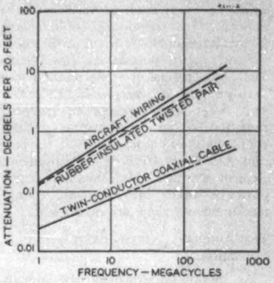

Figure 2. Attenuation of various types of transmission lines

The field about flexible conduit is commonly known as a "leak," the inference being that radio waves or even the field between the inner and outer conductors actually leaks out between the wires composing the weave. The term "leak" is unfortunate, as it does not describe adequately what is taking place. The same term, also used incorrectly, is applied to describe the fields set up by current flows at the mating surfaces between the magneto and the distributor, or between the magneto and the engine. A slight consideration will show that the spaces where such "leaks" are believed to take place are so much smaller than the length of the wave in question that actual leakage cannot take place. The remedy ordinarily prescribed to cure this "leak," that of grinding the mating surfaces so that contact will be uniform, simply provides a low-impedance path for the current, so that it is not forced to seek a path which lies outside the magneto.

Common Impedance Coupling

Mention has been made previously as to the introduction of radio noise into a receiver by the formation of a radio-influence voltage across the impedance between the receiver chassis and ground, the impedance being directly in the antenna circuit of the receiver. Such cases are more common than is realized, and are more responsible for radio noise than is ordinarily believed. Inasmuch as any such voltage will of necessity be small, the only manner in which the radio receiver can be affected is by the inclusion of one of these impedance elements in the antenna circuit, and no other circuit than the antenna need be considered.

Medium and High Frequencies

These frequencies will be discussed in some detail before proceeding to the very high-frequency case, as both have many points in common, and the very high-frequency case is more easily understood after reference has been made to the medium- and high-frequency cases.

With reference to Figure 3, it will be noticed that radio-influence currents entering the receiver over any of the possible paths (power wiring, control wiring, and so forth) must flow to structure through the ground impedance, which is composed of the bonding connector, and any other impedance path present, all being in parallel. This impedance is of a very complex nature and may be either capacitative or inductive, depending upon the installation and the frequency. This is because the impedance of a bonding connector goes up rapidly with frequency, while the reactance offered by the capacitance between the chassis and structure decreases with frequency. In any event, a complex voltage is formed across the impedance, and an inspection of the block diagram will show that this voltage is directly in series with the antenna circuit. For all intents and purposes, then, there are two generators in the antenna circuit. First, the equivalent signal generator, which has an internal impedance equal to the antenna impedance and an output voltage equal to the signal induced in the antenna by the electromagnetic waves from the station being received. Second, the equivalent radio-influence source, which has an internal impedance equal to the ground impedance and a voltage equal to the product of the radio-influence current through it and the ground impedance. This is shown in Figure 3. The voltage which appears across the input circuit of the receiver is the resultant of the vector addition of the signal and radio-influence currents times the impedance of the input circuit. Since signal voltages are apt to be low, it will not require much current flowing through the bonding impedance to provide a voltage at least equal to the signal voltage and probably much larger.

It is apparent that the installation of coaxial cable or twisted pairs in the leadin on many installations other than on aircraft are effective in controlling "noise," not because induced "noise" voltages are balanced out, but because common impedance coupling is eliminated. It may be very difficult to make a separation test that will differentiate between coupling to the leadin and common impedance coupling, except in an all-metal airplane where the leadin is adequately shielded.

Three conditions must be observed in any separation test to determine the effect of common ground-impedance coupling. These are:

1. Exclude from the antenna circuit that portion which is exposed to direct coupling; that is, the antenna itself.

2. Retain within the antenna circuit all current-flow paths that are present in the normal circuit; that is, the ground connections at the receiver and the antenna mast should remain intact.

3. Replace the antenna with an equivalent impedance. At medium frequency and high frequency this may consist of a capacitor approximately equivalent to the capacitance of the antenna to the fuselage; at very high frequency the impedance should approximate that of the antenna at the frequencies being considered.

All conditions must be observed, or else the separation test will be invalid. The first condition is more or less obvious, but not the second and third. However, it must be remembered that, if the antenna-circuit-impedance relations are changed, the various portions of the test cannot be correlated, and the test results will be meaningless.

VERY HIGH FREQUENCY AND ULTRAHIGH FREQUENCY

Very high-frequency and ultrahigh-frequency receivers are invariably coupled to the antenna by some form of coaxial cable, with the outer sheath ordinarily of a flexible weave to facilitate installation and maintenance. At the receiver and at the antenna this cable terminates in a special fitting, so that it may be disconnected readily, again to facilitate installation and maintenance. Each of these breaks in the coaxial cable run introduces an additional impedance, however slight. The greatest source of extraneous impedance elements are these fittings, especially where the antenna circuits have not been designed with the elimination of radio noise in mind. A typical example, using a current very high-frequency transmitter–receiver, will be given.

The coaxial fitting is on top of the transmitter–receiver (an error to begin with, as any currents induced in the coaxial cable sheath have a relatively long path to travel to ground and can get into all sorts of mischief along the way); the center conductor is brought down to the antenna switching relay, where, by means of long leaf-type spring contacts, connection is made either to the receiver or transmitter. The connection to the receiver input coil is well routed, but the input coil is grounded directly to the chassis, forcing the antenna signal currents to travel back to the coaxial fitting over the best path possible. The coaxial cable fitting is grounded to the chassis by means of the mounting bolts and a copper lead, but is held about an inch off the main chassis by the mounting bolts, so that the sheet-metal-case cover is flush with the base of the coaxial fitting.

Several impedance elements are apparent here: the impedance of the fitting itself, the impedance between the fitting and the chassis, and the impedance between the ground point of the coaxial fitting and the receiver input coil. Obviously, the total impedance between the end of the coaxial cable and the ground point of the receiver input coil will raise the fitting above ground potential, as far as the receiver is concerned. It has been observed that, with the antenna terminal shielded, touching or even bringing the finger close to the coaxial fitting will bring the receiver up to full output level, if even a weak field is present.

On this same transmitter–receiver the coaxial cable fitting to the antenna mast introduces another long lead from the fitting to the ground stud on the mast. Ordinarily not too much attention is paid to the ground connection between this stud and structure, so this may also be of high impedance, particularly if ordinary bonding braid is used. The bonding connector here should be as short as possible and preferably of copper strap.

Ordinary flexible shielding, such as is used on shielded leads on interphone systems, has been shown to have an attenuation of 60 decibels, and it is very unlikely that fields are present inside the airplane fuselage of sufficient strength to induce voltages in the inner conductor of a coaxial cable. There are, however, fields due to standing waves present upon the aircraft wiring, and wherever wiring and coaxial cables run in parallel, it is a very simple matter to set up a current flowing along the outer surface of the sheath.

The interference that may result from current flow in coaxial cable sheaths has been investigated thoroughly. A test setup of the receiver previously mentioned was made substantially as follows: all control and power wiring to the receiver was isolated and shielded by copper sheeting, all joints being well soldered, and all possible coupling to the receiver from this path eliminated. A coaxial cable with solid dielectric insulation, standard sheath weave and vinyl covering was extended about 15 feet away, the free end suitably terminated in the characteristic impedance of the cable with the terminal impedance also shielded. A very high-frequency signal generator was set up with an insulated coupling lead two feet long taped to the coaxial cable. With a fixed output of the signal generator the receiver output level was noted before and after the coaxial cable fitting was bonded to the chassis.

The results of this test in terms of decibels rise above the background level (reference level, one milliwatt in a 600-ohm line) were 24-decibel rise with the coaxial fitting unbonded; two-decibel rise when bonded.

It thus was concluded that common impedance coupling was responsible for the increases in output level. To test the accuracy of this conclusion, the antenna connector of the receiver was terminated at the coaxial fitting and shielded so that no coupling could occur; then the sheath of the coaxial cable was attached to the fitting. Coupling to the coaxial cable was accomplished as before, and it was found that, with approximately the same signal level from the signal generator as before, the output level of the receiver was 27 decibels above the background level. The installation of a bond on the coaxial fitting caused a decrease in output level of 21 decibels.

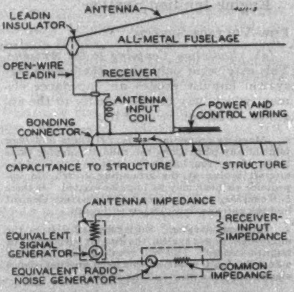

Figure 3. Common impedance coupling

A. Block diagram of typical radio-receiver installation at medium and high frequency

B. Equivalent circuit diagram usable for any frequency range

To summarize, it is evident that a receiver, although fitted with a coaxial cable, may be affected by currents in the coaxial cable sheath. This is accomplished by the currents flowing through a relatively high impedance common to the antenna circuit of the receiver. Such an impedance may be one formed at any portion of the antenna circuit by a discontinuity, or in any portion of the circuit in which both the antenna current and the radio-influence currents flow over the same path.

Elimination of Very High-Frequency Radio Noise

The proper starting point in the elimination of radio noise at any frequency is in the design of the radio receiver. Unfortunately, the designer usually is concerned mostly with circuit design, the antenna being only the means of introducing a signal of certain characteristics, so that something else happens at the output end of the receiver. Bench testing is done under conditions which do not approximate those in service with the result that a receiver which performs perfectly under test may be noisy when installed in the airplane. Then in an effort to eliminate the interference various methods of interference suppression are used, including filters upon all items of suspected electric equipment, bonding, and shielding.

Interference suppression by the means enumerated has become a sort of obsession, so much so that suppression methods are applied by specification requirements to all equipment, whether or not they produce radio noise. In view of the demonstrated fact that much of this interference could be eliminated by proper design of the radio receiver, it is at once apparent that the suppression devices added are largely dead weight, decreasing the pay or bomb load, as the case may be.

Lest the whole burden of the blame be shifted too quickly to the radio-equipment designer, it should be said here that the blame rests equally upon him, the designer of the electrical equipment producing the interference, and the designer of the radio installation in the airplane. A very great deal can be done by the equipment designer, for, while we know of no way as yet to correlate the radio-influence voltage of a device with the radio noise this device produces in the receiver, we do know that a motor can be designed to create a minimum of radio-influence voltage. The electrical-equipment designer must have not only the performance of his design in mind; he must consider its effect upon other equipment. The extra weight in filters, and so forth, which is necessary to make one item of equipment comparable in all respects to another item is directly chargeable to that item.

The designer of a radio installation may destroy entirely the good work of the radio-equipment designer and the electrical-equipment designer. Long antenna leadins are sometimes unavoidable, but it is inexcusable to place wiring or rotating equipment in close proximity to that leadin without taking adequate precautions so that there will be no interference. But inasmuch as the radio-installation designer is confronted with the problem of making presently available equipment function properly, it is necessary that this designer know the methods which are practical and workable for the reduction and elimination of radio noise.

To this end, in the section to follow, the general outlines of a test program are given, specifically for very high-frequency radio-noise tests. Before any such test is made the test man must make himself thoroughly familiar with the airplane upon which he is working insofar as the electric and ignition system are concerned. All ignition-system wiring should be traced out, both on the wiring diagram and on the airplane itself. The installation of the radio equipment should receive the most careful consideration, the feed points of the power supply, the location of the control equipment and wiring to it, and the run of the coaxial cable from the antenna to the receiver; all should be well fixed in mind. After this, the test man should determine the points at which the separation tests are to be made. Finally, he should set down in a logical sequence the tests he intends to make, and this sequence should be followed at all costs during the tests. This latter is especially important, as it is very easy to allow oneself to be distracted during the tests. The result is that invariably the most important test is left out, and a guess will have to be made at the results that should be on hand.

Test equipment need not be elaborate. There should be prepared beforehand a list of needed equipment, which of course will be dictated to some extent by the available equipment and the experience of the test man. A complete listing will not be given here, but three items must be considered:

1. *Antenna termination.* This should be a shielded box, containing an impedance approximately equal to that of the antenna it is to replace with a fitting, so that the coaxial cable can be attached, and another fitting, so that the box may be attached to where the coaxial cable normally goes. This is important, as it preserves the continuity of the ground circuit of the coaxial cable sheath.

2. *Shielded oscillator.* This oscillator should be fitted so that it can be attached to the magneto pencils or one of the spark plugs. In this way, tests can be made upon the effect of the magneto grounding wires, or loose coupling and gland nuts may be found while the engine is not running.

3. *Duplicate receiver.* The radio receiver as mounted in the airplane may be surrounded by intense fields created by wiring carrying radio-influence currents, and probe tests made with this receiver may be misleading. It is desirable to have a duplicate receiver for such tests, complete with power supply. If the receiver and power supply are mounted on a metal panel and placed on a dolly, they may be moved as convenience dictates and will prove very useful.

No attempts should be made to interpret the results of the probe test in terms of absolute field strength; this is virtually impossible. Any comparisons made should be on the basis of a qualitative analysis only. Field-strength measurement at any frequency is a laboratory test; this is especially true at very high frequency.

VERY HIGH-FREQUENCY RADIO-NOISE TEST ANALYSIS

Inasmuch as the main radio-influence source at very high frequency is the engine ignition system, this source alone will be considered here. As the preceding text has indicated, the ignition system may be broken down conveniently into two parts: the high-voltage and low-voltage sides. These will be taken up separately, and the various coupling paths will be considered.

Coupling Paths from the Aircraft-Engine Ignition System

LOW-VOLTAGE SYSTEM

(*a*). *Radiation Coupling to the Antenna.* This may occur only where the low-voltage system impulse excites an impedance element which may couple directly to the antenna. This can be done by:

1. Magneto grounding leads. If there is a poor joint at any place along the conduit carrying these leads, or any coupling between these leads and the aircraft wiring at the grounding switch, an impedance element may be impulse-excited. If there is a coupling path between this impedance element and the antenna, interference will result.

2. Ignition vibrator. Since this is fed from the aircraft d-c power system, the aircraft wiring may be impulse-excited. This will result in interference if a coupling path is present between any impedance element of the wiring and the antenna.

(*b*). *Conduction Coupling.* This occurs where currents are induced by any means whatsoever in any portion of the antenna system other than the antenna itself. It may be caused by the magneto grounding leads or the ignition-vibrator feed wire impulse-exciting any portion of the aircraft wiring system.

HIGH-VOLTAGE SYSTEM

(*a*). *Radiation Coupling to the Antenna.*

1. Direct coupling. This may occur where any portion of the high-voltage ignition system is in such condition as to set up a local field which couples directly to the antenna.

2. Coupling through some other impedance element. Radio-frequency currents fed through any path, either the magneto grounding leads or the booster coil leads, may excite some impedance element from which there is a coupling path to the antenna.

(*b*). *Conduction Coupling.* This is analogous to conduction coupling in the low-voltage system and occurs where radio-frequency energy is coupled into the aircraft wiring system from the magneto grounding leads or the booster coil, inducing a current into some impedance element of the antenna circuit.

The breakdown given may seem oversimplified, but it is all there. It is important to realize that there are only a few coupling paths from the aircraft-engine ignition system, and then plan the test procedure to isolate them one at a time.

As a basic test procedure, the following is suggested: Pull the magneto grounding pencils and then disconnect the leads to the booster or ignition vibrator. If radio noise still persists, stop the engines and feed a signal from the oscillator into one of the spark-plug leads. A probe test will show which coupling is loose; often it is necessary only to short-circuit across the suspected connections with a screw driver to check the effectiveness of the electrical path through them.

Next, with the ignition harness cleaned up, run the engines with the booster or ignition coil connected and the magneto grounding pencils disconnected, then vice versa.

This procedure definitely will indicate the coupling paths; it then will devolve upon the test man to determine the circuit elements through which coupling is occurring.

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Altitude Rating of Electric Apparatus

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THE proper functioning of electric equipment is of prime importance in the operation of the military and naval airplane of today. The wide use of such equipment and the continual increase in the operating altitude of aircraft make it essential that the factors affecting the operation of electric apparatus at high altitudes be investigated if the apparatus is to be designed properly for this new and expanding application.

This paper studies the effect of altitude on the ratings of rotating electric machines and, after determining the fundamental principles involved, discusses these in relation to the application of such machines in modern aircraft. It is shown that the rating of a self-ventilated direct engine-driven aircraft generator decreases rapidly with altitude and, at some altitude within the operating range of the airplane, the generator may be able to dissipate only its no-load losses. It is shown also that an air-scoop-ventilated direct engine-driven aircraft generator maintains its rating over most of the operating range of present-day aircraft. Finally, certain sea-level tests are proposed from which calculations of rating under altitude conditions can be made.

The theory and tests presented in this paper have been checked by altitude-chamber studies on an aircraft-type d-c generator.

Fundamental Analysis

Heat Transfer in Electric Machines

One of the principal factors determining the rating of electric apparatus is the maximum temperature to which its insulation can be subjected without failure during the required life of the machine. This operating temperature is a function of the losses of the machine and its ability to dissipate the resultant heat.

An electric machine may dissipate its losses by four means:

1. Forced convection between winding and core surfaces and cooling air.

2. Free convection between frame and ambient air.

3. Radiation between frame and surrounding objects.

4. Conduction between frame and mounting.

The watts loss which may be dissipated by these processes is a function of the temperature difference between the machine surfaces and the ambient temperatures. The machine temperature rises until the total watts loss dissipated just equals the input losses; that is, until equilibrium is reached. Thus, any increase in load (and hence, loss) will increase the machine temperature unless the additional heat can be removed from the machine by an increased heat transfer. Conversely, any reduction in heat transfer will increase the machine temperature unless the loss is reduced.

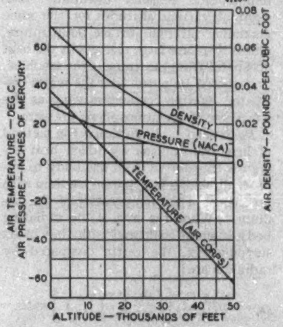

Figure 1. Properties of atmospheric air

The heat transfer between the machine and the ambient air also is affected by the air density. Thus, at the reduced densities and temperatures encountered at altitude (Figure 1), the watts loss which can be dissipated by an electric machine may change, and its rating at altitudes may be different from that at sea level.

Each of the heat-transfer processes is now considered individually, and its variation with density and temperature determined.

Forced Convection. The transfer of heat by the forced circulation of a gas or liquid is referred to as heat transmission by forced convection. In an electric machine the cooling air enters the machine at an ambient temperature Θ_{a1}, and in wiping the windings and core removes heat from these parts. The resultant increase in air temperature ("air rise") is denoted by Θ_{ar}. This process is shown graphically in Figure 2, where Θ_{s1} is the surface temperature of the windings or core of the machine and Θ_{d} is the temperature drop between the hot surface and the cooling medium ("surface drop").

The maximum allowable winding surface temperature is determined by the insulation used in the machine. Thus, if Θ_{s1}', Θ_{d}', and Θ_{ar}' denote values of surface temperature, surface drop, and air rise, respectively, at the point at which the ventilating air leaves the machine,

$$\Theta_{s1}'=\Theta_{a1}'+\Theta_{ar}'+\Theta_{d}' \text{ degrees centigrade} \quad (1)$$

Θ_{ar}' is a function of the weight flow of air through the machine and the watts loss taken away by this air. Thus,

$$\Theta_{ar}'=\frac{0.133\,q_{for}}{W} \quad (2)$$

where

q_{for} = watts loss dissipated by forced convection

W = weight flow of cooling air through machine, pounds per minute

$W=\rho Q$

ρ = density of cooling air at entrance to machine, pounds per cubic foot

Q = volume flow of cooling air at entrance to machine, cubic feet per minute

The surface drop at the exit of the machine is:

$$\Theta_{d}'=\frac{q_{for}}{C} \text{ degrees centigrade} \quad (3)$$

where

C = over-all thermal conductance between machine surface and cooling air, watts per degree centigrade

The thermal conductance C is a function of the density of the cooling air ρ, its velocity v past the surface being cooled, and the area of that surface A_1. It has been shown (reference 1) that for the turbulent flow of air in ducts

$$C \propto A_1(\rho v)^{a} \quad (4)$$

In an electric machine the velocity v is composed of two components: the axial velocity of the cooling air through the ducts of the machine v_a, and the peripheral velocity of the rotor v_p. Tests on d-c aircraft-type generators indicate that for ratios of axial to peripheral velocity approximately less than 2, the axial velocity can be neglected; whereas, for ratios greater than 2, the peripheral velocity can be neglected. In the practical case of self-ventilated and separately ventilated aircraft generators, the former type usually has a ratio of axial to peripheral velocity well below 2, and the latter type has a ratio well above 2. Hence, the transition range need not be studied closely in the present investigation.

The axial velocity v_a is:

$$v_a=\frac{144Q}{A_d} \text{ feet per minute} \quad (5)$$

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where

A_d = total area of ducts through which the cooling air flows, square inches

Thus, from equations 4 and 5 and the afore-mentioned assumptions,

$$C_{self} \propto A_1 v_p{}^{\alpha} \rho^{\alpha} = \frac{1}{k_1}\,\rho^{\alpha} \tag{6}$$

$$C_{sep} \propto A_1\left(\rho\frac{144Q}{A_d}\right)^{\alpha} \propto A_1\left(\frac{144}{A_d}\right)^{\alpha} W^{\alpha} = \frac{1}{k_2}\,W^{\alpha} \tag{7}$$

where k_1 and k_2 are constants for any given machine operating at constant speed, and C_{self} and C_{sep} are the over-all thermal conductances for self-ventilated and separately ventilated machines, respectively.

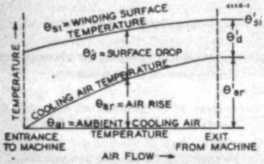

Figure 2. Temperature–air-flow diagram of a single-end axially ventilated electric machine

In smooth ducts, $\alpha = 0.8$; in rough ducts, α may vary from 0.5 to 1.0, depending on the degree of roughness. On one aircraft-type d-c generator the use of an exponent of 0.5 checked test data.

Substitution of equations 2, 3, 6, and 7 into equation 1 gives:

Self-ventilated:

$$q_{for} = \frac{\Theta_{s1}' - \Theta_{a1}}{\dfrac{0.133}{W} + \dfrac{k_1}{\rho^{\alpha}}} \text{ watts} \tag{8}$$

Separately ventilated:

$$q_{for} = \frac{\Theta_{s1}' - \Theta_{a1}}{\dfrac{0.133}{W} + \dfrac{k_2}{W^{\alpha}}} \text{ watts} \tag{9}$$

Hence, if Θ_{s1}', Θ_{a1}, ρ, and W are known at a given altitude, the watts loss which can be removed by forced convection at that altitude can be found if k_1, k_2, and α are known. These constants can be determined from sea-level tests on the machine.

Free Convection. The transfer of heat by the natural circulation of a gas, caused by a nonuniform temperature (and hence density) variation within the gas, is referred to as heat transmission by free convection.

The heat transferred from a horizontal cylinder in air is

$$q_{free} = 1.83 \times 10^{-4} A_2 p^{0.5} \frac{(\Theta_{s2} - \Theta_{a2})^{5/4}}{L^{1/4}} \text{ watts} \tag{10}$$

where

q_{free} = watts loss by free convection
A_2 = area of surface subject to free convection, square inches
p = atmospheric pressure, inches of mercury
Θ_{s2} = convecting surface temperature, degrees centigrade
Θ_{a2} = ambient air temperature for free convection, degrees centigrade
$L = \dfrac{L_h L_v}{L_h + L_v}$, feet
L_h = horizontal length of cylinder, feet
L_v = vertical height of cylinder, feet

The watts dissipated by free convection at any altitude therefore can be calculated from equation 10, since all factors are known. The surface temperature can be determined from sea-level tests. Tests taken under actual altitude conditions have indicated that the frame temperature remains essentially constant at various altitudes for the same maximum internal surface temperature, unless there are large changes in the heat dissipated from the frame of the machine.

It should be noted that the watts dissipated by free convection vary as the 0.5 power of the atmospheric pressure.

Radiation. Radiation is the method of heat transmission by which heat energy is transferred from one body to another through a transparent medium with no change in the temperature of the medium. When the area of the radiating body is small compared with that of its surroundings, the watts dissipated by radiation are:

$$q_R = 0.00375\epsilon A_3\left[\left(\frac{\Theta_{s3}}{100}\right)^4 - \left(\frac{\Theta_{a3}}{100}\right)^4\right] \text{watts} \tag{11}$$

where

q_R = watts dissipated by radiation
ϵ = emissivity of the surface of the radiating body (0.9 for a body covered with a nonmetallic paint)
A_3 = area of radiating surface, square inches
Θ_{s3} = radiating surface temperature, degrees centigrade absolute
Θ_{a3} = ambient air temperature or temperature of surrounding bodies (if close enough) for radiation, degrees centigrade absolute

The preceding equation shows that the watts dissipated by radiation are independent of air density or pressure and, hence, of altitude for constant Θ_{s3} and Θ_{a3}. The same statements concerning these temperatures apply to this case as to the case of free convection.

Conduction. The heat transferred between two bodies by the conduction process is directly proportional to the conductivity of the transmitting medium, its area, and the temperature difference between the two bodies, and inversely proportional to the length of path. In an electric machine heat is conducted through its mounting, the direction of the transfer depending on the relative temperature of the two bodies. This heat transfer is difficult to calculate, since the length of path and the temperature difference are not accurately known. However, it is not affected by air density or temperature, provided the temperature of machine and mounting remain constant. Measurements of these temperatures must be made to determine the direction of heat flow, and the results obtained used to temper the calculations of total heat transferred by the other three processes. If heat is removed from the machine it can be neglected and used as a "safety factor;" if it is added, an estimate of the quantity added may be necessary.

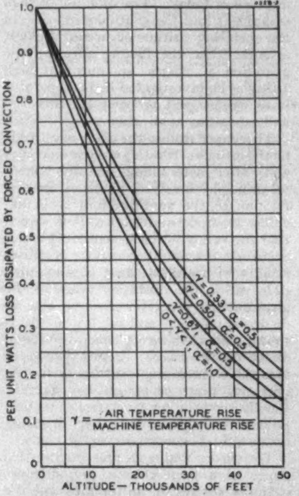

Figure 3. Effect of altitude on the watts loss dissipated by forced convection from a self-ventilated direct engine-driven aircraft generator

Constant maximum generator-winding surface temperature—140 degrees centigrade

Constant engine-accessory-compartment temperature—38 degrees centigrade

SUMMARY OF HEAT-TRANSFER EQUATIONS

$$q_{tot} = q_{for} + q_{free} + q_R \text{ watts} \tag{12}$$

Self-ventilated:

$$q_{for} = \frac{\Theta_{s1}' - \Theta_{a1}}{\dfrac{0.133}{W} + \dfrac{k_1}{\rho^{\alpha}}} \text{ watts} \tag{8}$$

Separately ventilated:

$$q_{for} = \frac{\Theta_{s1}' - \Theta_{a1}}{\dfrac{0.133}{W} + \dfrac{k_2}{W^{\alpha}}} \text{ watts} \tag{9}$$

$$q_{\text{free}} = 1.83\times10^{-4}A_2 p^{0.5}\frac{(\Theta_{s2}-\Theta_{a2})^{5/4}}{L^{1/4}}\text{ watts} \qquad (10)$$

$$q_R = 0.00375\epsilon A_3\left[\left(\frac{\Theta_{s3}}{100}\right)^4-\left(\frac{\Theta_{a3}}{100}\right)^4\right]\text{ watts} \qquad (11)$$

AIR FLOW IN ELECTRIC MACHINES

All the terms in the heat-transfer equations have been discussed, except the weight flow of the air used to cool the machine W. This quantity varies with altitude in a manner which depends on the type of ventilation employed. In the case of aircraft equipment, two types of machine are used: the enclosed self-ventilated machine, where the cooling air is circulated by a fan integral with the machine; and the enclosed separately ventilated machine, where the cooling air is picked up by an air scoop on the airplane and forced through the machine by the ramming pressure obtained from the airplane's motion. Single-end axially ventilated machines are discussed here, as this is the type in most common use on the equipment considered in this paper.

When the friction pressure drop of the air in flowing through the passages of a machine is neglected (a good assumption for small machines), the equation governing the air flow through a machine is:

$$H=\frac{\rho}{5.2}\left[\beta_m\frac{v_m^2}{2g}+\beta_n\frac{v_n^2}{2g}+\ldots\right]\text{ inches of water*} \qquad (13)$$

where

H = pressure head available for forcing cooling air through machine, measured at entrance to machine, inches of water

$\beta_m\frac{v_m^2}{2g}, \beta_n\frac{v_n^2}{2g}, \ldots$ = loss in pressure at contractions, expansions, bends, and so forth, of the air-flow path, measured in velocity heads of air

Since

$$v_m, v_n, \ldots = \frac{144Q}{A_m}, \frac{144Q}{A_n}, \ldots$$

$$H=\frac{Q^2\rho}{58}\left[\frac{\beta_m}{A_m^2}+\frac{\beta_n}{A_n^2}+\ldots\right] \qquad (14)$$

where

$A_m, A_n, \ldots$ = areas at changes in path section, square inches

The bracketed expression is a constant for any machine and may be called an "air resistance," R. Hence,

$$Q=\sqrt{\frac{58H}{\rho R}}\text{, cubic feet per minute} \qquad (15)$$

The weight flow of cooling air, therefore, is:

$$W=\rho Q$$

$$W=\sqrt{\frac{58H\rho}{R}}\text{, pounds per minute} \qquad (16)$$

This equation defines the weight of cooling air flowing through the machine for a given pressure head H. This pressure head will vary with altitude, depending on whether the machine is self-ventilated or separately ventilated by the methods discussed previously.

The pressure head developed by a fan revolving at constant speed varies directly with the air density. Therefore, in a self-ventilated machine the weight flow of air is directly proportional to the air density (equation 16).

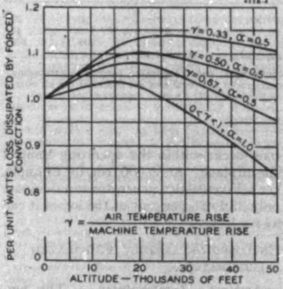

Figure 4. Effect of altitude on the watts loss dissipated by forced convection from a separately ventilated direct engine-driven aircraft generator

Constant maximum generator-winding surface temperature—140 degrees centigrade
Constant ramming-head cooling-air pressure
Cooling-air properties taken from Figure 1

For a separately ventilated machine employing an air scoop, the head H will depend on the airplane's speed–altitude characteristics. For a constant angle of attack the lift on an airplane wing is proportional to ρV^2, where ρ is the air density, and V is the velocity of the airplane relative to the air. Since a certain minimum lift is necessary to support the airplane, the minimum ρV^2 must remain constant, regardless of altitude. However, the pressure head in inches of water developed in the air scoop is directly proportional to ρV^2 (neglecting losses); hence the minimum air-scoop pressure head is independent of altitude for a separately ventilated generator. From equation 16 it can be seen that the minimum weight flow of air is thus proportional to the square root of the air density and therefore decreases with altitude, but not so rapidly as in the self-ventilated machine, where the decrease was directly proportional to the air density.

Application of Fundamental Analysis to the Determination of the Altitude Ratings of Rotating Electric Machines in Aircraft

The fundamental equations for heat transfer in rotating machines will be used now to determine the rating of electric machines under varying altitude conditions. The specific cases of self-ventilated and separately ventilated direct engine-driven d-c generators will be chosen for this discussion, each generator being considered separately. These generators are overhung from the back end of the aircraft engine. The self-ventilated generator secures its cooling air from the space back of the engine, known as the engine-accessory compartment; and the separately ventilated machine secures its cooling air from the atmosphere external to the airplane.

The general approach to the problem of altitude ratings is:

1. The determination of the ambient-temperature and air-flow conditions for each type of generator.

2. The calculation of the total watts loss which can be removed by the heat-transfer processes already discussed (heat transfer by conduction will be neglected) at the altitude at which the rating is desired.

3. The determination of the current, and hence the rating of the machine, corresponding to the total watts loss found in step 2 from a curve of watts loss of the machine as a function of its line current.

ENCLOSED SELF-VENTILATED GENERATOR

Ambient Temperatures. The ambient temperature of the cooling air Θ_{a1} of a self-ventilated generator is that of the engine-accessory compartment. Tests have shown that this temperature is independent of altitude. Present information indicates that a minimum compartment temperature of 38 degrees centigrade and a maximum of 65 degrees centigrade may be expected. This ambient temperature is also the temperature to be used in calculating the heat transfer by free convection, Θ_{a2}.

The generator frame will radiate to or acquire heat from surrounding objects, depending on the relative temperature of the bodies involved. This transfer will remain substantially independent of altitude.

Air Flow. Previous equations have shown that the weight flow of air W through a self-ventilated generator is directly proportional to the air density and therefore decreases with altitude.

An inspection of equations 8, 10, and

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*This equation assumes that the air density is constant throughout the machine. Because of the air temperature rise this is not strictly true. However, the simplicity obtained by assuming a constant density warrants its use. For more accurate calculations an average air density may be used rather than the value at the machine entrance.

11 now shows that the watts loss which may be dissipated by a self-ventilated generator, for a constant maximum-allowable surface temperature, decreases with increase in altitude. All quantities in these equations are constant except the air flow W, the air pressure p, and the air density ρ, all of which decrease with altitude. The reduction in rating depends on the relative proportions of q_{for}, q_{free}, and q_R acting to cool the generator. Table I shows the per cent watts loss dissipated by each of the three heat-transfer processes at an altitude of 36,000 feet as compared with the dissipation at sea level (taken as 100 per cent). For example, if the generator could dissipate all of its loss by radiation alone, its rating would be independent of altitude, since q_R is independent of air density and pressure. In any actual application all processes will enter, but the watts dissipated by forced convection will be at least 80 per cent of the total for the lightweight high-output generators now used in the modern airplane and therefore will be the limiting feature.

Table I

Comparison of the Watts Loss Dissipated From Self-Ventilated Direct Engine-Driven Aircraft Generators Employing Different Proportions of Heat-Transfer Processes in Their Cooling

Per Cent of Total Watts Dissipated at Sea Level*			Per Cent of Sea-Level Watts Dissipated at 36,000 Feet*			
By Forced Convection $\gamma=0.67$, $\alpha=0.5$	By Free Convection	By Radiation	By Forced Convection $\gamma=0.67$, $\alpha=0.5$	By Free Convection	By Radiation	Total
100	0	0	27	0	0	27
0	100	0	0	47	0	47
0	0	100	0	0	100	100

* Maximum generator-winding surface (140 degrees centigrade), generator-frame surface, surrounding object, and engine-accessory-compartment (38 degrees centigrade) temperatures are constant for all comparisons.

Figure 3 shows the reduction in the watts loss that may be dissipated by forced convection in a self-ventilated generator, as a function of altitude, for constant maximum surface temperature and constant generator speed. Curves are plotted for various values of α in equation 8, and for various ratios of air rise to machine temperature rise at sea level γ. In self-ventilated machines this latter ratio is usually greater than 0.5.

After the total watts loss which can be dissipated by the three processes of heat transfer at a given altitude has been calculated, the new current rating of the generator at that altitude is found from the curve giving the generator line current as a function of its watts loss. This curve is approximated by a constant, representing the no-load losses of the machine plus a term which is a function of the square of the line current.

If the watts which can be dissipated at the operating altitude of the airplane are less than the no-load losses, the machine cannot even maintain its voltage at no load. Figure 3 shows that the self-ventilated generator thus has a "ceiling" of operation at which point the machine can dissipate its no-load losses only and can carry no load current without exceeding its maximum allowable temperature. Since the no-load losses of a lightweight high-output generator may be one quarter to one third of its total losses when carrying rated current, this ceiling may occur within the operating limit of the airplane. At 35,000 feet, for example, the self-ventilated generator can dissipate only 25 to 35 per cent of the losses it can at sea level.

Enclosed Separately Ventilated Generator

Ambient Temperatures. The ambient temperature of the cooling air Θ_{a1} of a separately ventilated generator is that of the air outside of the airplane (neglecting a small temperature rise of the air at the entrance to the air scoop due to its compression). The Air Corps standard atmospheric air temperature decreases with altitude at a rate of −2 degrees centigrade for every 1,000-foot increase in altitude (Figure 1). The ambient temperatures for free convection and radiation remain the same as for the self-ventilated generator.

Air Flow. The minimum ramming-head cooling-air pressure available at the air scoop of an airplane is independent of altitude. From equation 16, therefore, the minimum weight flow of air in a separately ventilated generator is proportional to the square root of the air density.

Equations 10 and 11 show that the same watts loss is dissipated by free convection and radiation in the case of the separately ventilated generator as for the self-ventilated machine.

Figure 4 shows the variation in the watts loss that may be dissipated by a separately ventilated generator for constant ramming-head cooling-air pressure, constant maximum surface temperature, and constant generator speed. Curves are plotted for various values of α in equation 9 and for various ratios of air rise to machine temperature rise at sea level γ.

After determining the total watts loss which can be dissipated by the three processes of heat transfer, the altitude rating of the generator can be found from the curve of watts loss as a function of line current, as was done for the self-ventilated generator.

From Figure 4 and equations 10 and 11 it is apparent that a separately ventilated generator, which obtains its cooling air from an air scoop on the airplane, maintains and may even increase its sea-level rating at practically all altitudes within the operating range of present-day aircraft. In the case of the separately ventilated generator, the reduction in cooling-air ambient temperature offsets the decrease in weight flow of cooling air. Also, due to the constancy of the ramming-head cooling-air pressure of the separately ventilated generator, the weight flow of cooling air decreases only with the square root of the air density. In the self-ventilated gen-

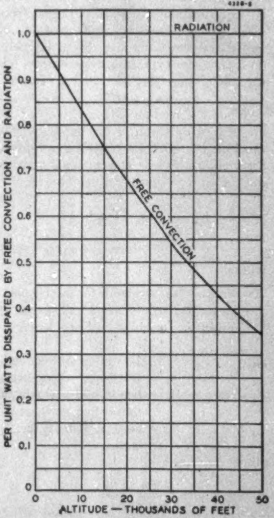

Figure 5. Effect of altitude on the watts loss dissipated by free convection and radiation from self- and separately ventilated direct engine-driven aircraft generators

Constant generator frame surface, engine-accessory compartment, and surrounding object temperatures erator the weight flow decreases directly with the air density.

Conclusions

A. Figure 3 shows that the rating of an enclosed self-ventilated direct engine-driven d-c aircraft generator decreases rapidly with altitude, and may have a "ceiling" within the altitude range of the airplane on which it is mounted. This ceiling is the point at which the generator will just dissipate its no-load losses without overheating.

B. Figure 4 shows that the rating of an enclosed separately ventilated direct engine-driven d-c aircraft generator, obtaining its cooling air from an air scoop mounted on the airplane, remains essentially independent of altitude—the rating increasing slightly over its sea-level value up to 20,000 feet and then decreasing again.

C. Sea-level tests can be taken, by means of which the rating of a given generator at a given altitude can be determined if ambient conditions at that altitude are known (see appendixes).

D. The analysis presented in this paper can be used to determine the air-flow requirements necessary to obtain a given rating from a given generator at any desired altitude (see appendixes).

E. The results of this analysis can be applied to the determination of the altitude heating and rating of types of electric apparatus other than aircraft generators, if the assumptions made here are critically reviewed and adapted to meet the new conditions.

F. While the effect of brush and commutator heating on limiting the altitude rating of a generator has not been discussed, this problem can be analyzed using the formulas applying to a self-ventilated machine, since whether the machine is self- or separately ventilated the peripheral velocity of the commutator is large compared with the axial velocity of the cooling air (see *Forced Convection* under *Fundamental Analysis*).

G. This paper presents some of the fundamentals of the problem of altitude rating of electric machines and has proposed tests for its determination. It is hoped that future test data, obtained both in altitude chambers and under actual operating conditions, will be correlated and used to modify and expand the analysis where necessary.

Appendix A. Sea-Level Tests and Altitude Performance Calculations for Generators Designed for Aircraft Applications

The tests proposed here are based on the assumption that the point at which the maximum-allowable surface temperature Θ_{s1}' is measured at sea level remains the hottest point in the machine as the load on the machine is varied. Tests on aircraft-type generators have indicated that this assumption is correct. In the proposed tests the generator mounting should be insulated thermally from the test stand on which it is mounted, in order to eliminate the effects of conduction. It is also assumed that the outer surface of the generator remains essentially at constant temperature for a constant maximum internal surface temperature. This assumption has been checked by altitude-chamber tests.

A. Preliminary Tests

These preliminary sea-level tests determine the weight flow of cooling air forced through the generator for various ramming-head pressures at the entrance to the generator cooling system, the watts loss of the machine for various line currents, and the value of the exponent α in equations 8 and 9.

1. Measure the volume flow of air through the generator for various inlet ramming-head air pressures. Set up the generator as a separately ventilated machine and operate at minimum rated speed. Vary the volume flow from a value at which the ratio of the axial velocity of the cooling air to the peripheral velocity of the rotor is approximately 2, to a value at which this ratio is approximately 4.

2. Plot a curve of weight flow of air as a function of the ramming-head pressure of cooling air from the results obtained in section *A*-1.

3. Calculate or determine from dynamometer tests the watts loss of the generator as a function of its line current with the generator operating at its minimum rated speed and rated maximum-allowable surface temperature. Plot a curve of total watts loss as a function of line current.

4. Take a series of heat runs on the generator at minimum rated speed at various inlet ramming-head air pressures, covering the range of pressures used in section *A*-2. At each pressure vary the generator load current until the temperature of the hottest surface in the machine is equal to the maximum-allowable surface temperature. Keep the generator ambient temperature Θ_{a2} at the same value as is expected during actual operation.

5. Calculate the watts dissipated by free convection and radiation in each of the runs of section *A*-4 from equations 10 and 11. Subtract these values from the total watts dissipated in each of the runs, and plot a curve of watts dissipated by forced convection as a function of inlet ramming-head air pressure.

6. Rewrite equation 9 as:

$$\log k_2+(1-\alpha)\log W = \log\left[\frac{W(\Theta_{s1}'-\Theta_{a1})}{q_{for}}-0.133\right]$$

Plot W as a function of

$$\left[\frac{W(\Theta_{s1}'-\Theta_{a1})}{q_{for}}-0.133\right]$$

on log–log paper for various values of inlet ramming-head air pressure. Select a value of this pressure and find q_{for} from the curve of section *A*-5 and W from the curve of section *A*-2 for this pressure. Using these values of W and q_{for}, plot one point of the aforementioned curve. Repeat, using a different pressure.

7. Obtain the slope of the curve of section *A*-6 and calculate α, knowing that this slope is equal to $(1-\alpha)$.

B. Self-Ventilated Generator

To find the altitude rating of a self-ventilated aircraft generator, proceed as follows:

1. Take a heat run at minimum rated speed at such a line current that the temperature of the hottest surface in the machine is equal to the maximum-allowable surface temperature for the generator insulation. Take this heat run in an ambient temperature equal to the expected engine-accessory-compartment temperature at the altitude at which the generator is to be rated.

2. Measure the volume flow of air Q through the machine under the ambient conditions of section *B*-1, and calculate the weight flow of cooling air through the machine.

3. From the watts loss-current curve of section *A*-3, determine the total watts loss of the machine at the load of section *B*-1.

4. Calculate the watts dissipated by free convection and radiation in the heat run of section *B*-1 from equations 10 and 11.

5. Determine the watts dissipated by forced convection in the heat run of section *B*-1 by subtracting the watts dissipated by free convection and radiation from the total watts dissipated.

6. Calculate k_1 in equation 8, using temperatures and the weight flow of air found in sections *B*-1 and *B*-2, the value of α found in section *A*-7, and the value of q_{for} found in section *B*-5.

7. Calculate the total watts dissipated at the altitude at which the generator rating is to be determined from equations 8, 10, and 11. Use values of air density and pressure, and engine-accessory-compartment temperature at the given altitude. The weight flow of air in a self-ventilated generator is directly proportional to the air density. Use values of Θ_{s1}', Θ_{s2}, Θ_{s3}, Θ_{a1}, Θ_{a2}, and Θ_{a3} from the heat run of section *B*-1.

8. Determine the altitude current rating of the generator by entering the watts loss-current curve of section *A*-3 with the total watts dissipated as calculated in section *B*-7

C. Separately Ventilated Generator

To find the altitude rating of a separately ventilated generator, proceed as follows:

1. Determine the minimum ramming-head cooling-air pressure in inches of water available at the entrance to the generator cooling cap at the altitude at which the rating of the generator is to be found.

2. Determine the watts loss that can be dissipated by forced convection by the generator with this ramming-head pressure at sea level from the curve of section *A*-5.

3. Calculate the value of k_2 from equation 9, using the watts loss found in section *C*-2, the weight flow of air from section *A*-2 (correcting for any difference between the cooling-air temperatures of the runs in sections *A*-2 and *C*-2 in determining the weight flow), the temperatures from the heat runs of section *A*-4, and the value of α from section *A*-7.

4. Calculate the total watts dissipated at the altitude at which the generator rating is to be determined from equations 9, 10, and 11. Use values of air pressure and cooling-air temperature and density from Figure 1. The weight flow of cooling air in a separately ventilated generator is proportional to the square root of the cooling-air density. Use values of Θ_{s1}', Θ_{s2}, Θ_{s3}, Θ_{a2}, and Θ_{a3} from the heat run of section *A*-4, corresponding to the minimum ramming-head cooling-air pressure of section *C*-1.

5. Determine the altitude current rating of the generator by entering the watts loss-current curve of section *A*-3 with the total watts dissipated as calculated in section *C*-4.

Appendix B. Determination of the Altitude Rating or the Required Ramming-Head Pressure of a Separately Ventilated Aircraft Generator

By the use of certain simplifying assumptions the current which a separately ventilated generator will deliver at altitude, or, conversely, the ramming-head pressure required to obtain a given current at altitude, can be determined from a single sea-level heat run on the generator.

In the following analysis the value of α in equation 9 is assumed to be unity; the effects of convection, radiation, and conduction in cooling the machine are neglected; and the watts loss of the machine is assumed to vary as the square of the generator line current. Assuming α to be unity gives a higher value of the watts which may be dissipated by forced convection than is actually the case, but the omission of the watts dissipated by other processes offsets the effect of this first assumption. Finally, in a separately ventilated generator, the I^2R losses are usually at least three to four times the fixed losses, so that at the generator's normal rating the total losses vary approximately as the square of the current.

From equations 7 and 16, therefore:

$$q_{for} = K_3 I^2 = \frac{W}{0.133 + K_2}(\Theta_{s1}' - \Theta_{a1})$$

$$= \frac{\sqrt{\dfrac{58\rho H}{R}}}{0.133 + K_2}(\Theta_{s1}' - \Theta_{a1})$$

$$I = K_4 \sqrt[4]{\rho H(\Theta_{s1}' - \Theta_{a1})^2}$$

If we let the subscripts A denote the value of a quantity at any altitude, and 0 denote the value at sea level, then:

$$I_A = I_0 \sqrt[4]{\left(\frac{\rho_A}{\rho_0}\right)\left(\frac{H_A}{H_0}\right)\left[\frac{(\Theta_{s1}' - \Theta_{a1})_A}{(\Theta_{s1}' - \Theta_{a1})_0}\right]^2} \text{ amperes} \tag{17}$$

or:

$$H_A = H_0\left(\frac{\rho_0}{\rho_A}\right)\left(\frac{I_A}{I_0}\right)^4\left[\frac{(\Theta_{s1}' - \Theta_{a1})_0}{(\Theta_{s1}' - \Theta_{a1})_A}\right]^2 \text{ inches of water} \tag{18}$$

From a single sea-level heat run, then, I_0, ρ_0, H_0, and $(\Theta_{s1}' - \Theta_{a1})_0$ are determined. Knowing these, and the density, inlet air temperature, and allowable hot-spot temperature at the altitude at which the generator is to operate, the current which the machine will deliver without exceeding its temperature rise for a given ramming-head cooling-air pressure can be calculated from equation 17. Also, the ramming-head pressure required for a given current rating can be calculated from equation 18.

Altitude-chamber tests have been taken, and altitude ratings and pressure heads calculated from equations 17 and 18 have checked almost exactly with test values.

For a more fundamental calculation of altitude performance, the methods of Appendix *A* should be used.

Nomenclature

A_1 = area of machine surface for forced convection, square inches
A_2 = area of machine surface for free convection, square inches
A_3 = area of machine surface for radiation, square inches
A_d = total area of machine ducts through which cooling air flows, square inches
β_m, β_n = numerical constants depending on type of change of air-flow section
C_{self} = over-all thermal conductance between machine surface and cooling air for self-ventilated machines, watts per degree centigrade
C_{sep} = over-all thermal conductance between machine surface and cooling air for separately ventilated machines, watts per degree centigrade
ρ = density of cooling air at entrance to machine, pounds per cubic foot
ϵ = emissivity of radiating surface
$\gamma = \dfrac{\text{air temperature rise}}{\text{machine temperature rise}}$
g = gravitational constant, feet per minute squared
H = pressure head available for forcing cooling air through machine, inches of water
$L = \dfrac{L_h L_v}{L_h + L_v}$, feet
L_h = horizontal length of machine for free convection, feet
L_v = vertical height of machine for free convection, feet
p = atmospheric pressure, inches of mercury
q_{for} = watts loss dissipated by forced convection
q_{free} = watts loss dissipated by free convection
q_R = watts loss dissipated by radiation
Q = volume flow of cooling air through machine measured at entrance, cubic feet per minute
R = resistance of machine to flow of cooling air
Θ_{a1} = ambient air temperature for forced convection, degrees centigrade
Θ_{a2} = ambient air temperature for free convection, degrees centigrade
Θ_{a3} = ambient air temperature for radiation, degrees centigrade absolute
Θ_{ar} = cooling-air temperature rise (air rise) degrees centigrade
Θ_d = temperature difference between machine surface and cooling air (surface drop), degrees centigrade
Θ_{s1} = machine surface temperature for forced convection, degrees centigrade
Θ_{s2} = machine surface temperature for free convection, degrees centigrade
Θ_{s3} = machine surface temperature for radiation, degrees centigrade absolute
v = velocity of cooling air past machine surface being cooled, feet per minute
v_a = axial velocity of cooling air through ducts of machine, feet per minute
v_p = peripheral velocity of rotor, feet per minute
W = weight flow of cooling air through machine, pounds per minute

Primed quantities are values measured at the cooling-air exit of the machine.

Reference

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A-C and D-C Short-Circuit Tests on Aircraft Cable

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Synopsis: Higher-voltage d-c and 400-cycle a-c electric systems are being developed for large aircraft with increased electrical load in order to obtain lower conductor weights than would be required with the present 28.5-volt system. In view of the apparent freedom from sustained faults in low-voltage d-c systems despite damage from gunfire, the use of protective devices to isolate faulted cables has been considered unnecessary. The need for protective devices on the higher-voltage systems will depend on the relative behavior of faults and the required clearing time.

A laboratory investigation based on four representative types of cable fault was made to determine (1) reasons for the apparent freedom from sustained faults in present 28.5-volt systems, and (2) comparative fault-clearing ability on the higher-voltage systems.

The tests show (1) that freedom from sustained faults on 28.5-volt systems can be attributed mainly to fraying of faulted cables and the high level of fault current at most locations, and (2) that ability to clear is not materially different at the higher voltages if as much fault current is available.

Current levels were determined, as a function of cable size, above which faults caused by gunfire can be expected to burn clear without serious fire hazard. Where available fault currents are below these values protective devices may be required to eliminate fire hazard.

For 400-cycle systems protective devices may be required to clear faults with minimum generation in time to prevent loss of synchronism or stalling of motors.

ELECTRICAL-load growth in military aircraft has been accelerated by the need for greater firepower, range, and bomb capacity. Larger aircraft of all classes require power operation of some flight-control surfaces and auxiliary apparatus. The latest cargo planes incorporate hoist motors and fuel-transfer-pump motors to facilitate loading and handling. Future passenger planes will require additional electric power for safety devices and luxury items designed for passenger comfort. Increased system voltage has been required to keep pace with load growth and greater circuit length because of cable weight required for satisfactory voltage regulation. Copper weight in military aircraft has increased rapidly to the stage where the 28.5-volt system is no longer adequate for proposed aircraft. Generators, motors, and other equipment have been designed for 120-volt d-c and 208/120-volt three-phase 400-cycle a-c systems.

One of the many problems in the development of the 120-volt d-c and 208÷120-volt 400-cycle systems is protection against power failure caused by cable faults. The high degree of reliability experienced with 28.5-volt d-c systems in spite of the large amount of damage inflicted during combat indicates that cable faults burn clear easily and need not be isolated by protective devices. Theoretical considerations indicate the possibility that cable faults will not clear as well on the new higher-voltage systems. Behavior of faults under various conditions and the factors governing ability to clear must be determined before the use of these voltages can be recommended without protective devices to clear faults. It is also desirable to know whether there are critical conditions which may cause failure to clear on 28.5-volt systems, and whether higher voltages than those now contemplated will materially affect fault characteristics. For these reasons laboratory tests of simulated cable faults were made at various levels of alternating and direct voltage and fault current at both atmospheric and reduced air pressure.

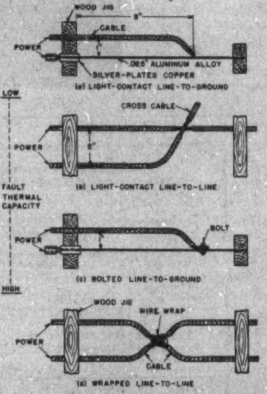

Figure 1. Method of representing aircraft cable faults

Before discussing fault characteristics some criteria for evaluating results should be determined. A reliable electrical system must: (1) be able to withstand faults without creating serious fire hazard; (2) limit the time without power for essential loads to a tolerable duration; and (3) return to satisfactory operating condition with little or no loss of load.

Any short circuit may start a fire under unfavorable conditions, but at least the fault should not cause the cables to burst into flames and the amount of molten metal which splatters from the arc should not be sufficient to set fire to the surrounding materials.

In commercial and cargo planes, power to electrically operated flight-control surfaces and engine accessories may usually be interrupted for several seconds and sometimes even minutes without serious danger. On the other hand, in combat aircraft, the loss of essential loads for only a fraction of a second at a critical moment during attack may result in complete destruction. Military planes returning from combat evidence minor difficulty from these causes, even when the structure is badly damaged from gunfire. However, this does not prove that temporary loss of electric power or damage from short circuits is not responsible for the loss of planes. The increasing use of rockets, aerial bombs, and heavier cannon projectiles undoubtedly will increase the number and severity of faults.

During a heavy short circuit motors decelerate and a-c generators drift apart in phase relation. The ability of the system to return to normal parallel operation and reaccelerate motor loads is a function of the duration of the fault. The permissible time for d-c systems is relatively long but for 400-cycle systems it is in the order of one-tenth second.

For all of the above reasons it is essential that faults be cleared rapidly, especially on combat airplanes with a-c systems. If self-clearing characteristics do not provide this speed, a distribution system with protective devices for clearing faults must be provided.[1]

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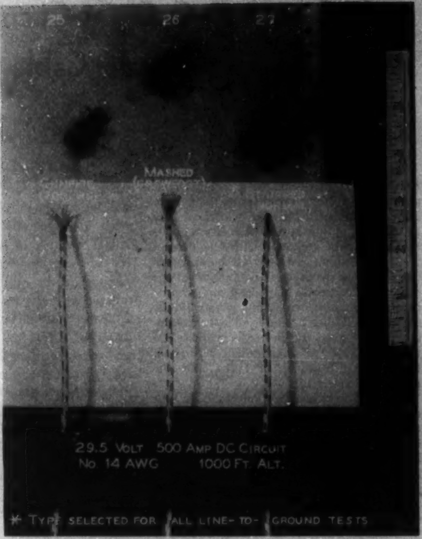
Aircraft Cable Faults

Specific information on the nature and number of cable faults encountered in aircraft is not available. A representation of aircraft cable faults in the laboratory cannot reasonably take into consideration all the possible variables. If aircraft-cable-fault tests were to be completely rigorous, it would be necessary to include a wide range of fault-contact pressure, area, and thermal capacity. Essentially, however, there are two extremes of cable faults. At one extreme are faults caused by frayed conductors where only a few strands are in light contact with a metal structure. At the other extreme are faults involving one or two cables where the conductors are pinned by or against a large metal object such as the plane structure. In the former case, the thermal capacity in the fault is less than that of the cable, while in the latter case the thermal capacity in the fault is greater than that of the cable. The two types may be termed respectively "low-thermal-capacity" and "high-thermal-capacity" faults. It must be remembered that with low-thermal-capacity faults cables are usually in motion because of magnetic forces and vibration or changing attitude of the airplane.

Laboratory Representation of Faults

To determine self-clearing ability, tests were made with voltage, available fault current, air pressure, fault thermal capacity, and cable size as the variables. The difficulties in obtaining conclusive data from cable-fault tests are well known in industrial practice. The problem in aircraft circuits is even more complex because of the scarcity of data on the number and type of faults, type of fault contacts, and contact pressures involved. Since specific information was not available, logical assumptions were made and test procedure and conditions established so as to determine comparative effects of different system voltages and fault-current levels. From the results of these tests the clearing ability of a random fault in a particular system can be estimated. Where clearing is questionable suitable protection can be provided to insure system reliability or to eliminate fire and explosion hazards.

Aircraft cable in 28.5-volt systems range in size from number 20 American wire gauge to as large as 3/0. For the following reasons three sizes, 20, 14, and 8 were selected from the above range for the short-circuit tests. (1) It is expected that with higher voltages and multiple-circuit feeders there will be no need for cables larger than about number 8 American wire gauge. (2) Experience shows that in faults on heavy cable, particularly those caused by gunfire, only a few strands of a severed or chafed cable will make fault contact, and the full cross section rarely will be involved. Such faults can be represented approximately by tests on smaller cables which have the same cross-sectional area as the active portion of the large cable. In interpreting the results, however, one must remember that the heavy conductor may clear and restrike a sufficient number of times to fuse the strands and permit more of the cross section to establish contact. Also, a large cable in comparison to a small cable will have stiffer spring action when subjected to magnetic forces and vibration, and greater contact pressure because of its weight.



25
26
27
MASHED
29.5 VOLT 500 AMP DC CIRCUIT
No. 14 AWG 1000 FT. ALT.
* TYPE SELECTED FOR ALL LINE-TO-GROUND TESTS

Figure 2. Three types of fault contact

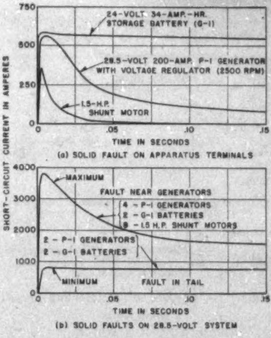


Figure 3. Short-circuit characteristics of d-c apparatus and a d-c system

In the case of faults resulting from gunfire damage, the probability of heavy contact pressure and restricted cable motion is extremely low. For the general case, it is assumed that most faults have low thermal capacity. The cable arrangements, shown in Figure 1a and b, were selected for these tests and constant conditions obtained by bending the severed cable upward to relieve all strain except that necessary to establish electrical contact. By using the same cable arrangement at all values of current and

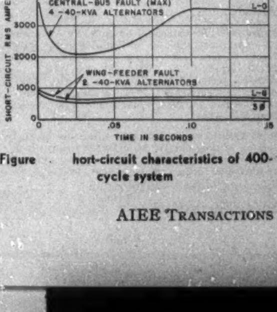


Figure . hort-circuit characteristics of 400-cycle system

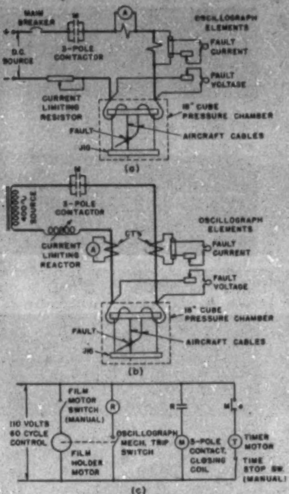


Figure 5. Test circuits

(a)—Direct current
(b)—Alternating current
(c)—Control

voltage, the effects of other variables such as cable size and air pressure can be analyzed. An angle of 45 degrees was assumed as an average contact position for line-to-ground faults. The cable placement used for low-thermal-capacity line-to-line faults is shown in Figure 1b. The severed cable rests lightly across the straight cable, where insulation has been removed, representing two cables spaced two inches apart that have been damaged and have fallen together. The cross cable overhangs the straight cable to prevent easy separation by magnetic forces.

Contact of the total number of wire strands of the various cable sizes was not realized in either of these arrangements. The 45-degree position for line-to-ground faults did not permit the full cross section to contact the aluminum alloy unless the strands were spread. A preliminary check was made on a 28.5-volt 500-ampere d-c circuit to determine the effect of contact area. Three types of line-to-ground faults are shown in Figure 2. The cables are not shown in position for test, but are placed, after the test, near the fault point on a sheet of white paper to illustrate arrangement of strands. The "spread" and "crowfoot" types used in tests 25 and 26, respectively, represent the two probable extremes in contact area for a given cable size. Test 27 shows the strands in normal position concentrated at the fault; it is essentially the same as test 25, except that more strands are in contact with the aluminum. The following observations were made during the tests:

Test 25. "Spread"—only a few strands in contact; sputtered and arced for a few tenths of a second. Cleared at fault after melting all exposed conductor and burning three-eighths-inch hole in aluminum alloy.

Test 26. "Crowfoot"—conductor red hot at fault for approximately two seconds, then gas pressure built up between insulation and conductor until released by a jet of smoke and varnish from the open end of the cable; conductor became red hot over entire length. Cleared first time at fault in about 4 seconds. Reapplied power—cleared in approximately 2 seconds. Reapplied power—conductor became red hot over entire length—melted clear outside jig in 2.35 seconds; three-eighths-inch bared conductor at fault not melted but welded lightly to aluminum alloy.

Test 27. "Normal"—strands uniform as received; cleared at fault in 2.2 seconds burning one-eighth-inch hole in aluminum alloy. At first, appeared to weld and conductor approached red heat at fault—then sparked and cleared. Reapplied power—no restrike. Jarred cable and reapplied power—restruck for 0.02 second.

The conditions represented by test 25 were considered too favorable to be representative of many faults, especially with small cable. The conditions in test 26 actually constitute high thermal capacity and were covered by other tests. Conditions similar to those in test 27 were used in all remaining tests to represent low-thermal-capacity faults.

In contrast to light-contact line-to-ground and line-to-line faults, when the conductor has been pinned against part of the plane structure or other metal objects there is much greater thermal capacity for radiating and conducting heat from the fault. High thermal capacity is simulated in line-to-ground fault tests by means of a bolt, Figure 1c, and in line-to-line faults by a heavy wrap of fine copper wire, Figure 1d.

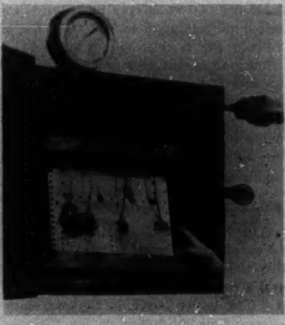


Figure 6. Low-pressure chamber

Table I. Available Fault Current

Direct Current		400 Cycles	
Open-Circuit Voltage	Maximum Initial Available Amperes	Open-Circuit Voltage	Maximum Initial Available Amperes (Symmetrical Rms)
28.5	2,200	120	2,100
	500		1,200
	200		1,640
	75		180
			60
120	3,500	208	1,800
	1,600		1,250
	625		690
	180		180
	63		60
240	3,500	254	1,200
	1,170		180
	600		
	425		
	195	440	800
	50		180
1,000	900	1,700	63
	130		

In order to limit the amount of heat conducted from the fault by the conductor itself, different cable lengths were used depending upon the fault thermal capacity. For example, light-contact line-to-ground fault tests were made with only two feet of cable; light-contact line-to-line tests were made with three-foot lengths; bolted line-to-ground and wrapped line-to-line tests were made with four-foot lengths.

Short-Circuit Characteristics of Aircraft Systems

In addition to establishing a basis for representation of cable faults, the short-circuit characteristics of existing aircraft systems must be examined in order to select power sources for the laboratory tests. The short-circuit characteristics of aircraft equipment were obtained in the laboratory and used in network calculator studies of typical large aircraft systems. In the cable-fault tests, no attempt was made to duplicate transient generator characteristics or specific circuit constants of existing systems. Instead, power sources were selected with sufficient capacity to maintain constant current at different levels covering the range of currents expected in service.

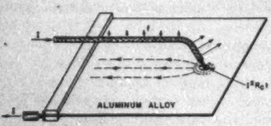
D-C Systems

Short-circuit current in d-c systems is contributed by generators, storage batteries, and motors. Current–time characteristics for a short-circuit on the terminals of a 28.5-volt 200-ampere shunt generator, a 34-ampere-hour battery and a 1.5-horsepower shunt motor are shown in Figure 3a. Generator and motor currents decay rapidly because the machines are shunt-excited.

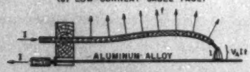
A network-calculator study of a 28.5-volt system, which has four 200-ampere generators and two 34-ampere-hour batteries, indicates that the maximum current for a solid fault approaches 4,000 amperes initial peak, and decays rapidly, as shown in Figure 3b. The sustained value of fault current at this location (not shown in the Figure 3b) is less than 1,000 amperes and is contributed almost wholly by the storage batteries. The minimum current delivered to a solid fault is 730 amperes. This fault is located on a fuselage feeder in the tail section, and two of the four generators are out of service. With all generators and batteries in service, the sustained fault current at this location is 1,040 amperes. Fault currents lower than 730 amperes may be obtained when a severed cable forms two simultaneous faults. Usually, in such rare cases, one of the faults clears rapidly and all available current is fed to the remaining fault.

There are 28.5-volt systems larger than that in the example which use 300-ampere generators and can deliver somewhat higher initial peak fault current. However, it was not necessary to test as high as 4,000 amperes since 2,200 amperes on number 8 cable gave self-clearing results which check combat experience.

A 120-volt d-c system consisting of four 200-ampere generators would have a maximum initial peak current of about 3,000 amperes without batteries or 4,000 amperes with two 34-ampere-hour batteries. The minimum sustained current would be about 1,000 amperes with batteries, but if the batteries were omitted, the current after approximately 0.05 second would be a function of generator speed and the circuit resistance between the generator and the fault. For a few tenths of a second compensated generators will deliver from 100 to 250 per cent of rated current depending on speed and external resistance. For longer periods of time, the current gradually decreases to zero if the external resistance is below the critical value for the generator and its operating speed. Therefore, without batteries, faults would have to clear in a few tenths of a second, for low values of external resistance, if appreciable current were required to provide clearing.

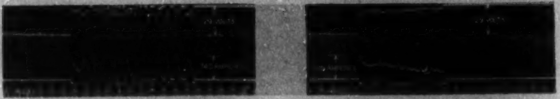


(a) LOW–CURRENT CABLE FAULT

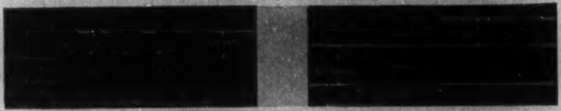


(b) HIGH–CURRENT CABLE FAULT

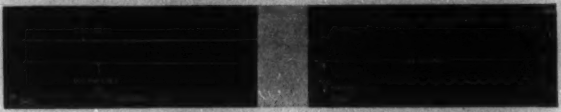
Figure 7. Magnetic forces aid clearing



(a)—Line to ground, number-8 American-wire-gauge conductor, 28.5 volts



(b)—Line to line, number-8 American-wire-gauge conductor, 120 volts



(c)—Line to ground, number-14 American-wire-gauge conductor, 240 volts

Figure 8. D-c faults at 1,000- and 40,000-feet pressure altitude

Film number in lower left corner. Fault current and fault voltage are zero until contactor closes

Of immediate interest in d-c distribution is the use of 120 volts. With future loads, 240 volts or possibly as high as 1,000 volts may be required for minimum conductor weight. The use of voltages higher than 500 for aircraft distribution systems may prove impractical because of the weight penalties imposed by greater insulation. However, a few tests in the range of 1,000 volts were made because such voltages may some day be required on short cable runs for high-power equipment.

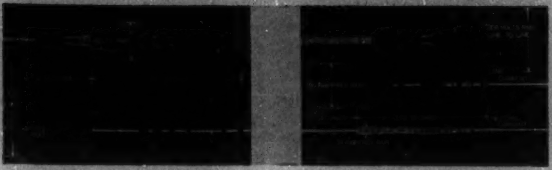
400-Cycle Systems

The approximate range and characteristics of fault current for a proposed 208/120-volt three-phase grounded-neutral 400-cycle system is shown in Figure 4. The upper curve is the maximum current-time characteristic for a single-line-to-ground fault as determined on the network calculator. The two lower curves are for single-line-to-ground and three-phase faults located on a 12 American wire gauge wing-feeder circuit, and are near the minimum fault-current level.

Test Procedure

The 28.5-volt power source consisted of 30 112-ampere-hour storage batteries. Power for 120-, 240-, and 1,000-volt tests was supplied by a 750-volt 1,500-kw motor-generator set. Generator voltage was adjusted by field control, and short-circuit current was limited to the desired value by external resistance. A 150-kva motor-generator set was used for 400-cycle tests with single-phase transformers totalling 1,000 kva. Different values of voltage and current were obtained by means of generator field-control, transformer reconnections and tap changes, and an adjustable reactor in the secondary. The over-all power factor of the test circuits was less than 20 per cent. Table I summarizes the range of system voltage and available fault current.

The test circuits are shown in Figure 5a and b. A large number of oscillograms was obtained for faults of less than two seconds duration. For faults that did not burn clear within two seconds, current was measured with an ammeter and time was measured by an electric timer. Figure 5c shows the control circuit used for all tests, which in sequence opened the oscillograph shutter, initiated the fault by closing contactor *M*, and started the electric timer. A few oscillograms were obtained for faults lasting longer



(a)

(b)

Figure 9 (above). Low-current fault on number-14 American-wire-gauge cable which required 30 seconds to clear

(a)—Line-to-ground light contact showing clearing and restrikes after a weld

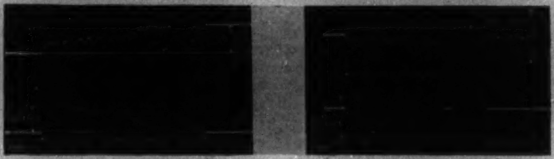
(b)—Line-to-line wrapped contact showing double-line-to-ground restrikes

Pressure altitude 40,000 feet

Figure 10 (below). Comparison of a-c and d-c high-current faults, line-to-ground light contact, number-8 American-wire-gauge

Available current:
Direct current, 3,500 amperes
Alternating current, 2,100 amperes rms

Pressure altitude 40,000 feet



Direct Current

400 Cycles

than 15 seconds by anticipating when the fault would clear, and opening the shutter in time to record clearing characteristics.

All tests were made inside an 18-inch-cube pressure chamber, shown in Figure 6. Atmospheric-pressure tests are designated as "1000-foot altitude" which corresponds to the elevation of the East Pittsburgh laboratories. High-altitude tests were made after exhausting the chamber to a pressure corresponding to an altitude of 40,000 feet.

For line-to-ground tests, 0.025-inch aluminum alloy, tarnished and dirty from exposure, was obtained from a scrapped fuselage. Occasionally it was necessary to clean the surface with emery paper in order to establish fault contact. Circuit connection to the aluminum was made with a minimum of contact resistance by clamping the aluminum sheet between two wide strips of silver-plated copper. These plates were connected to one of the pressure-chamber terminals. Aluminum sheet and jig were placed horizontally in the chamber for all tests. The jig cable clamps were spaced ten inches apart to approximate the distance between ribs in aircraft.

Analysis of Test Results

FACTORS WHICH PRODUCE SELF-CLEARING

Before the results of specific tests are given, a few remarks on the factors which bring about self-clearing may be helpful. For an analysis of circuit interruption phenomena the reader may refer to technical literature.[2,3,4]

In a d-c circuit, interruption of current flow is brought about by producing an arc which has a voltage drop, for all currents below the initial value, greater than the available voltage of the circuit at the point of fault. After the current becomes zero, the dielectric strength of the space between the two cables or cable and structure must increase more rapidly than the recovery voltage or a new arc will be formed.

Table II. Cable Damage With Line-to-Line Short Circuits—D-C Test

Test No.	Cable Size (American Wire Gauge)	Fault Contact	Pressure Altitude (Feet)	Volts (Open Circuit)	Fault Amperes (Rms)	Total Clearing Time (Sec)
30	14	Light	1,000	28.5	320	3.1
31	14	Light	40,000	28.5	320	3.1
161	14	Light	40,000	240	1,600	0.033
180	14	Light	40,000	120	175	26.0
74	20	Wrapped	40,000	120	550	0.120
132 *B*	20	Wrapped	1,000	240	440	0.195

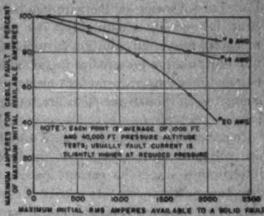


Figure 11. Percentage of available short-circuit current obtained with light-contact line-to-ground faults at 120 volts, 400 cycles

In a simple switch or contactor the arc is initiated and its length determined principally by contact travel. For larger circuit-interrupting devices at higher currents magnetic force on the arc and gas or oil blast also tend to increase the arc length or reduce its cross section to increase the arc voltage.

In a cable fault on a d-c circuit a similar action takes place but with two significant differences in the initiation and lengthening of the arc:

1. Relative motion between the two cables or cable and metal structure are produced primarily by vibration, or by magnetic forces which depend on the magnitude and distribution of the fault current. Even after the current has been interrupted a new contact will frequently be established because of the weight of the cable, its spring action, or vibration.

2. The thermal capacity of the cable and metal structure are usually sufficiently low that during a fault part of the conductor, the structure, and the surrounding insulation are vaporized and burned or blown away with explosive force.

For both of these reasons, low-current faults such as in Figure 7a are likely to be slow in clearing or may produce a weld since there is little magnetic force or heat available to produce or lengthen an arc. If contact is maintained, the heat generated at the point of contact may be only sufficient to cause partial fusion followed by a weld, or may eventually melt a hole in the aluminum alloy.

At some higher current the magnetic force will be great enough to lift the cable off the plate, forming an arc of sufficient length to interrupt the current such as in Figure 7b. The arc voltage may also be increased by magnetic "blowout," by melting the cable and aluminum, or by a blast of vaporized but un-ionized insulation material. After the current reaches zero, the cable will tend to drop back on the plate and re-establish contact unless sufficient conductor or aluminum has been burned away, or the wire has been bent beyond its elastic limit by the magnetic force. In addition a sufficient film of insulation material or metal oxide is sometimes formed to prevent electrical contact even though the parts are touching.

In a-c circuits the production of a high arc voltage is not necessary to produce interruption since the current becomes zero twice every cycle. It is only necessary that there be separation sufficient to give more rapid increase in dielectric strength than in recovery voltage. The natural deionization of the space between the conductors is aided by gas surrounding the arc. At low currents the "cold-cathode" recovery voltage may also aid interruption.

TEST RESULTS

(*1*). *Effect of Current Level.* From more than 300 a-c and d-c cable fault tests approximate current levels were determined above which the light-contact faults shown in Figure 1a and b cleared rapidly without welds or damage to insulation. Below these current levels, the tendency to weld increased. Minimum current levels were established where welds almost always occurred because the magnetic force on the cable was not sufficient to cause separation between two faulted cables or between the faulted end of the cable and the aluminum-alloy plate.

The total force which the magnetic field exerts to lift the cable from the plate may be calculated approximately by the formula[8]

$$F = 72I^2\left(\frac{l}{b}\right)10^{-8} \text{ ounces}$$

where l is the length and b is the separation between parallel conductors. Both cable weight and force are uniformly distributed along the cable as shown in Figure 7. With a fault current, I, equal to 500 amperes and with the length and separation as shown in Figure 1a the total force exerted is about 1.26 ounces. An 8-American-wire-gauge cable of this length weighs about 0.7 ounce. A force of 1.26 ounces would overcome cable weight but may not be sufficient to overcome spring force in the cantilever section of cable. However, with a fault current of 2,000 amperes the force would be 1.26 pound. A force of this magnitude would lift the cable rapidly and may bend it far enough to exceed the elastic limit, and reduce the possibility that a restrike may occur. It must be remembered that with faults to a skin-return surface, such as the aluminum-alloy plate, the return path of the current may not be directly under the cable. In a-c circuits, especially at a frequency of 400 cycles, reactance tends to force the current to return directly under the cable, but, in d-c circuits the path of least resistance will be followed.

In most faults which resulted in welds a small spark at the point of contact was observed at the instant power was applied to the faulted cable or cables. An oscillogram showing fault current and voltage for a weld-type fault is shown in Figure 8c, film 24B. The high concentration of energy melted the point contact and thereby increased contact area. If current were above the minimum level, the contact region was heated beyond a bright red glow and melted rapidly enough to cause separation and arcing. If current were below the minimum level, melting was retarded sufficiently to permit a further increase in contact area and the dissipation of heat energy by con-

Figure 12. Structural damage at low-current and 120 volts direct current

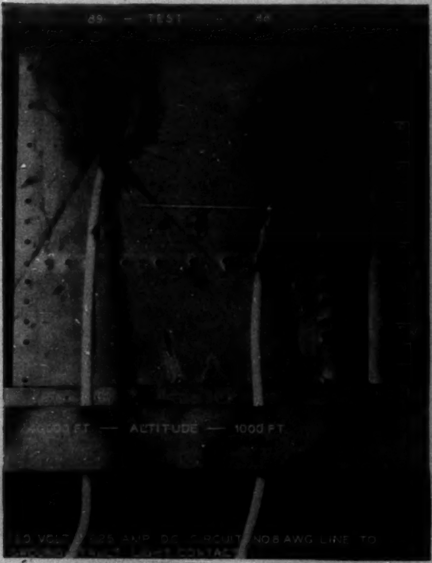
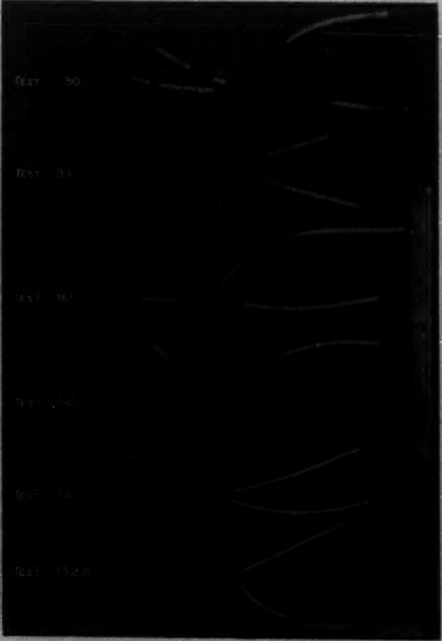


Figure 13. Cable damage with line-to-line short circuits, d-c test (see Table II)



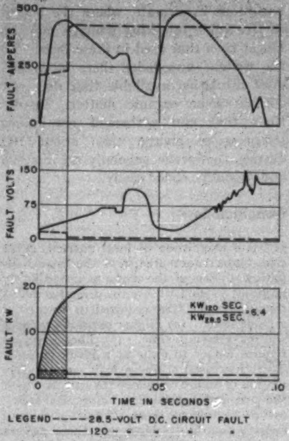


Figure 14. Arc sustained by higher voltage

duction and radiation. A change in color from bright red to dull red at the contact region gave visible evidence of heat dissipation. As current was decreased toward these minimum levels the light-contact faults behaved more like the high-thermal-capacity faults shown in Figure 1c and d. When clearing is effected by melting the cable because of resistance loss and without an arc, the melting may occur at the fault or at any point along the cable. If the aluminum alloy melts first, the action is sudden and is accompanied by brilliant flashes and explosive scattering of molten metal. For a fault duration of 30 seconds both high- and low-thermal capacity faults cleared at approximately the same fault-current value. The average values for these minimum currents as a function of cable size were as follows: 450 amperes with number 8, 175 amperes with number 14, and 60 amperes with number 20 American-wire-gauge cable. Insulation was destroyed and fires occurred with practically all faults at these currents. Behavior of faults was so consistent at these values of current that it was possible to anticipate the time at which clearing would occur, and delay the opening of the oscillograph film shutter until the fault was ready to clear. Figure 9a and b are oscillograms which show the clearing of line-to-ground and line-to-line weld type faults, respectively. For the test recorded in Figure 9b the transformer secondaries were wye-connected and the neutral connected to the aluminum-alloy plate. The original fault was from line to line but developed into a double-line-to-ground fault. This also occurred in many other tests of line-to-line faults.

With fault currents in the order of 1,000 amperes or more the magnetic force on the cables was of major importance in the behavior of faults and their ability to burn clear. Figure 10 shows oscillograms of high-current faults with 8-American-wire-gauge cable. In many cases the forces were sufficiently great that several interruptions and restrikes occurred before sufficient material was burned away to prevent the fault from being reformed. No welds or insulation damage occurred with light-contact faults when the root-mean-square value of fault current for the conducting period was more than approximately 2,000 amperes with number 8, 1,000 amperes with number 14, and 300 amperes with number 20 American-wire-gauge cable. The conducting time associated with these currents was in the range between approximately 0.1 second for 20 American wire gauge and 0.5 second for 8 American wire gauge. If a fault such as the wing-feeder fault on 12-American wire gauge cable, Figure 4, were to occur under the same conditions as used in the tests, there would be a good possibility of welding and insulation damage and also, the possibility of fire. In the tests, serious damage to insulation resulted in nearly all cases where welds occurred. There was little difference between light-contact line-to-line and line-to-ground fault clearing characteristics. In general, line-to-line faults cleared faster and seldom restruck.

Arc voltage in the case of light-contact faults may limit fault current to a value considerably less than the circuit can deliver to a solid fault at a given location. At a particular value of available fault current the limitation is greater in a 28.5-volt circuit than in a 240-volt circuit since arc voltage is a function of arc length and current magnitude. In one instance on a 240-volt circuit where initial peak available fault current was 1,060 amperes, the maximum fault current with a 20-American wire gauge cable was only 750 amperes. In a 28.5-volt circuit with about the same magnitude of available fault current, the maximum fault current with the same size cables was only 360 amperes. The ratio of actual peak fault current to available peak fault current increases with larger size cables but decreases at higher levels of available fault current. Also, much higher ratios are obtained with 400-cycle faults, as shown in Figure 11. This demonstrates that maximum values of available fault current, as determined by network-calculator studies, may be considerably higher than actual cable-fault currents, especially in 28.5-volt systems. In addition, even for a solid fault, current decreases with time because both metallic and contact resistances increase with temperature.

In general, damage to the aluminum alloy sheet was greater at low fault-current magnitudes. Because faults at low currents required longer time to clear, the temperature of the alloy was raised toward its melting point of 600 degrees centigrade. Then, when the arc formed, the explosive action blew a large hole where the alloy had melted (Figure 12). Tests at 120 volts, 400 cycles on 8-American wire gauge cable showed an exception to this rule in that structural damage increased with current magnitude. In both a-c and d-c tests the extent of damage was less for smaller cable sizes. With 20 American-wire-gauge cable the damage to the alloy sheet was negligible because the cable had less thermal capacity and radiating surface than the alloy.

Damage to insulation is shown by the cable samples in Figure 13 and Table II. Here again the extent of damage was greater at low current levels. Fire started in both test 30 and 31. In test 30 at atmospheric pressure the fire covered the entire length of cable and burned freely. In test 31 at reduced pressure the fire was quickly suffocated for lack of oxygen.

When the cables were heated by the passage of current, gas generated between conductor and insulation was ejected at high velocity from the open end of the insulation near the fault. This pressure increased more rapidly at high currents and gave a higher velocity jet which may have had a cooling or deionizing effect on the arc.

(2). *Effect of Voltage.* The best evidence of increased arcing at higher voltage was provided by aluminum damage in line-to-ground fault tests. The size of perforations increased almost in proportion to the voltage.

The effect of voltage on clearing time is not significant below 240 volts. At voltages of 240 and above, arcing-time may be a limiting factor in determining ability to clear. In one line-to-ground-fault test at 240 volts and one at 1,000 volts, both at 180 amperes direct-current with 14 American wire gauge and reduced pressure, arcing was sustained until the entire eight inches of free cable inside the jig had been fused. There was little damage to the alloy at these voltages because of the rapid fusing of the copper conductor. Occasionally, higher voltage may aid clearing by sustaining an arc which prevents welding. Figure 14 is a comparison of oscillographic records obtained for 28.5- and 120-volt line-to-ground faults. These two faults were selected because the circuits could supply approximately equal current to a solid fault and the faults involved similar peak and rms current values. Fault current in the 28.5-volt circuit, shown by the broken-line curve in Figure 14, was limited to a value of 200 amperes, then the arc was extinguished and the cable fell and welded to the alloy sheet. In the 120-volt circuit, however, with about the same conditions, voltage was sufficient to sustain the arc and the fault cleared in 0.32 second by fusing all exposed copper and burning a one-inch hole in the alloy. The current required to fuse a 14-American wire gauge copper conductor in 0.32 second is about 1,100 amperes. Thus, the energy responsible for clearing was not I^2Rt in the conductor but the EIt product of arc energy. In this example the fault energy, measured in watt-seconds, was approximately 6.4 times greater in the 120-volt test for the initial 0.012 second; this ratio increased for longer intervals of time. Since the ratio of power in the two circuits was only slightly greater than four, it appears that available arc energy increases with voltage. Arc energy, then, particularly during the initial 0.02 second, may determine whether a fault will weld or arc and burn clear. After a weld is once established, heat is conducted away from the fault so rapidly that the probability of clearing is greatly reduced.

(*3*). *Effect of Air Pressure.* Reduced air pressure did not have a noticeable effect on arcing time or clearing except in 28.5-volt tests. In 28.5-volt tests arcs usually were sustained at a pressure corresponding to an altitude of 40,000 feet, but at atmospheric pressure restriking occurred frequently. This contrast is shown by oscillograms in Figure 8a. At atmospheric pressure the required arc voltage exceeded the available voltage each time the cable was lifted by magnetic force. The conductor and alloy were burned only a small amount during each arcing period. At reduced pressure less arc voltage was required and the arc became almost stable. The continuous burning and vaporizing of copper and alloy resulted in faster clearing at 40,000-feet pressure altitude. Thus, reduced air pressure has an effect similar to that of increased voltage.

(*4*). *Effect of Thermal Capacity.* High thermal capacity in the fault (see Figure 1c and d) increased the time required to clear at a given current level. Greater damage to the insulation resulted because either the conductor or the alloy had to melt without the aid of an arc before clearing occurred. After the conductor had melted and parted, arcing and clearing was the same as for light-contact faults.

(*5*). *Fire Hazard.* Five types of aircraft cable that are insulated with either glass, asbestos, rubber, vinylite, rayon, or a combination of these, were tested at the 30-second current levels and none was found to be fireproof. Considerable amounts of volatile matter and gases were given off in the form of smoke when the insulation and slow-burning varnish or lacquer impregnants were heated to a temperature around 250 degrees centigrade. If the fire point were reached, combustion would occur. The fact that fire usually did not start until the time of clearing indicates that the combustible mixture was ignited by the high temperature in the arc when the conductor melted. At reduced pressure corresponding to an altitude of 40,000 feet the limited supply of oxygen in the pressure chamber was sufficient to support combustion for only a few seconds. One interesting example is shown in Figure 12. Welds were formed upon the initiation of the fault, both at 1,000- and 40,000-feet pressure altitude. In each case the fault cleared after a ten-second conduction period by melting the exposed wire back to the insulation and burning a hole one-inch wide and two-inches long in the aluminum alloy. In both instances the cables rested on the alloy at the edge of this hole with insulation burning; fire started at the time of clearing. At reduced pressure, the fire quickly suffocated because there was insufficient oxygen to support combustion. The 3.4-cubic-feet volume of air in the pressure chamber may not have been great enough to make this test representative of actual conditions. Fire in the case of the fault at atmospheric pressure however, continued to burn insulation near the fault. As the insulation burned and disintegrated, the wire moved closer to the edge of the hole in the aluminum alloy until contact was re-established and a restrike occurred. The restrike cleared rapidly, but fire continued and a succession of six restrikes followed in similar manner until the main circuit breaker was tripped by the operator after 30 seconds total time. Restriking probably would have continued until the entire free length of cable had been consumed. This type of fault would be extremely hazardous in aircraft, where the length of free cable following damage may be longer than that used in these tests. To the writers' knowledge there is no aircraft insulation available that does not contain some organic matter. Insulations that can withstand continuous temperatures greater than about 100 degrees centigrade generally are opaque and cannot be coded easily.

Conclusions

1. The magnitude of fault current, cable size, and contact area, and the heat-dissipating ability of the surfaces immediately adjacent to the fault are the principal determinants of the time required to burn clear a cable fault. (*a*) Higher current produces shorter clearing time. (*b*) Larger cables require longer to clear at a given current. Their increased stiffness may prevent formation of an arc or reduce the rate at which the arc is lengthened. Faults on heavy cables also have more contact area and radiating surface. However, these factors would not usually increase clearing time in proportion to the cable cross section because of the increased probability of fraying.

2. Damage to both insulation and structure and fire hazard are greater at low currents than high currents because of the much longer time required to clear. Damage would be reduced for currents which approach the normal capacity of both cable and structure. A completely fireproof insulation would be very desirable.

3. Voltage differences below 240 volts had no significant effect on total time to clear although the arcing time on d-c tests was increased slightly and damage to the aluminum sheet was increased. D-c faults at 240 volts showed appreciable increase in arcing time and a few tests at 1,000 volts showed prolonged arcing.

4. "High-thermal-capacity" faults, although unlikely to occur, required much longer to clear than "low-thermal-capacity" faults. The resulting damage and fire hazard were correspondingly greater. At very low currents this difference disappeared because of the long time required to burn clear either type of fault. Even the bolted and wrapped faults in Figure 1 did not produce faults with higher thermal capacity than the cable itself since such faults cleared with little damage at high currents by melting the bared section of wire adjacent to the fault.

5. Reduced air pressure caused some reduction in tendency to weld at 28.5 volts. At higher voltages on d-c faults the increase in arcing time became sufficient to constitute a disadvantage.

6. The greatest fire hazard resulted from insulation fires at low currents. Spattering of hot metal particles also produced some fire hazard at all currents. The action was more violent with d-c faults than with a-c faults.

7. Vibration will probably cause longer arcing time and more restrikes than were

Development of Excitron-Type Rectifier

H. WINOGRAD
MEMBER AIEE

METAL-TANK mercury-arc rectifiers have been in commercial use abroad since 1911, and in the United States since 1925. (There were several earlier experimental installations in this country, in 1913 and 1915.) Until recent years, all rectifiers were of the multianode type, with 6, 12, and 18 anodes in one tank. The trend had been to increase the number of anodes per tank to obtain a higher current rating per rectifier, and in Europe rectifiers with 24 anodes in one tank were developed. This trend was reversed in the United States with the introduction of the single-anode ignitron-type rectifier. The single-anode rectifiers are assembled in groups of 6 or 12 to make up one unit.

The chief advantages of the single-anode rectifier over the multianode type are:

1. Lower arc drop and consequently higher efficiency.
2. Simpler maintenance, as individual tanks can be removed and replaced without affecting the other tanks of the group.
3. Lends itself to large-scale production, as a larger number of smaller parts is involved.
4. Less sensitive to temperature variations.

These advantages outweigh the advantages of a simpler excitation circuit and fewer seals of the multianode rectifiers.

The excitron is the latest addition to the family of rectifiers. It is a single-anode rectifier in which a cathode spot is maintained continuously on the surface of the mercury cathode, when in operation, by means of a pilot (excitation) arc, and the firing of the anode is controlled by means of a grid. That is its basic difference from the ignitron-type rectifier, in which the cathode spot is ignited every cycle and in which the timing of the ignition is used to control the firing point of the anode.

In developing the excitron, the aim was to produce a rectifier which has the advantages of a single-anode rectifier while using the relatively simple and stable excitation and grid-control systems of the multianode-type rectifiers. In the present paper will be outlined the design problems involved in the development of the excitron, its design features, and the test methods used during its development.

Design

Design Problems

A question of fundamental importance in connection with the development of the excitron was the effect of the excitation arc at the cathode on the arc-back tendency of the rectifier during the nonconducting period of the anode, when it is at a negative potential with respect to the cathode. It was believed at one time that a rectifier in which no arc exists during this part of the cycle would be immune to arc-backs.[1,2] It was found, however, from extensive tests and experience, that in rectifiers operating in the range of voltages now in general use most arc-backs occur at the conclusion of the firing period of an anode, and that the major cause of arc-backs is the residual ionization in the vicinity of the anode when it ceases firing.[3,4] The residual ionization is a function of the anode current, the mercury vapor density, and the internal design of the rectifier. A small pilot arc at the cathode was found to have a negligible, if any, effect on the ion current to an anode during its nonconducting period. This is discussed more fully in another section of this paper.

In a multianode rectifier, the cathode has to be insulated from the tank to prevent establishment of cathode spots on the tank walls. It has been determined by tests that if the tank is connected solidly to the cathode there is a flow of a moderate current over this connection, which is due to the collection of ions by the tank walls. However, occasionally a cathode spot is developed on the tank wall causing transfer of the main-arc current from the cathode to the tank, since the solid connection between them permits an unimpeded flow of current from the tank to the load circuit.[5] In a single-anode rectifier, in which the cathode spot is initiated every cycle and is extinguished at the conclusion of the firing period of the anode, this problem is of little importance. Even if a cathode spot were established on the tank wall while the anode is firing, it would disappear when the anode current reached zero.

Sufficient cooling surface has to be provided for condensing the mercury vapor and for transmitting the heat generated by the rectifier losses to the cooling medium, at the maximum rating of the rectifier, while maintaining the mercury-vapor pressure within the limits required for satisfactory operation. It is desirable to operate the rectifier with a cooling-water temperature of 50 to 60 degrees centigrade so that a heat exchanger of reasonable size might be used for cooling the water.

Suitable arc shields or baffles have to be provided for directing the flow of mercury vapor from the cathode and the heat of radiation from the anode, to meet the conditions outlined in the preceding paragraph.

Other design problems are of a mechanical nature: vacuum-tight seals, materials capable of withstanding the high temperatures, evacuating connections and vacuum valves, design of details and assembly suitable for manufacture and convenient for servicing.

Excitron Design

In the first design of the excitron, shown in Figure 1, some of the elements of the multianode-type rectifier were incorporated. The cathode is insulated from the tank. In addition to the main anode and control grid, it has two excitation anodes and one ignition anode. The

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Most of the tests described in this paper were conducted at the Alcoa plant of the Aluminum Company of America. The author expresses his gratitude to the engineers of that company for their whole-hearted co-operation in this work.

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obtained in these tests. Also, under actual conditions the electric system is energized and the cable is in motion when a short-circuit occurs. Both of these factors may help to reduce the probability of welds.

8. Protective devices to isolate cable faults should be used where the available fault current can be low enough to produce serious fire hazard or result in a weld. It may be necessary to provide protective devices on 400-cycle systems to assure clearing in a short enough time (approximately 0.1 second) to prevent loss of synchronism or stalling of motors. In a d-c system ample battery capacity or separate generator excitation may be required unless faults can be cleared in a few tenths of a second.

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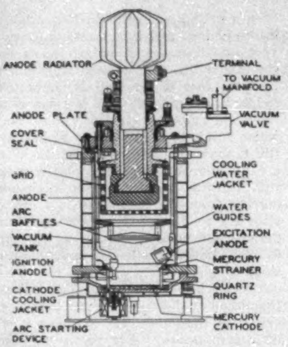


Figure 1. Cross section of earlier excitron design

arc at the cathode is started by a jet of mercury shot at the ignition anode by means of a nozzle-shaped plunger actuated by a coil over the plunger housing. The arc is maintained by the excitation anodes, which are connected to a low-voltage single-phase transformer and operate as a single-phase full-wave rectifier circuit carrying a direct current of about seven amperes. The connections to the ignition and excitation anodes are brought through insulating seals in the top flange of the tank. The anode head is enclosed by a perforated basket-type control grid. The grid connection is brought through the anode plate. Arc baffles are interposed between the anode space and the cathode.

The active part of the cathode mercury pool is enclosed by a quartz ring. The condensed mercury is returned to the cathode through a strainer ring. The cathode plate and tank are cooled by water circulating through water jackets, which are provided with water guides to direct and increase the speed of the water flow.

Units of this type of design were installed for 250-volt steel-mill service and for electrochemical service. While the performance of these units was satisfactory, development work was continued with a view of simplifying the construction. The chief aim was to eliminate the insulating seal between the tank and cathode and to reduce the number of auxiliary anodes. This led to the design shown in Figure 2.

In this design, the tank and cathode form a single structure. The problem of preventing arcing to the tank, with a continuous arc maintained at the cathode, was solved by installing a cooling coil which is insulated from the tank and which shields it from the arc.

The construction of the cooling coil is shown schematically in Figure 3. It is made of heavy-walled seamless steel tubing and consists of two coaxially wound sections joined by a circular header. An assembly of the cooling coil with the anode plate is shown in Figure 4. The adjacent turns of the coil are separated by narrow gaps, which permit the passage of gases and mercury vapor but exclude arc-through. The coil is made into a rigid structure by four rows of tack welds between turns. The coil terminals pass through the anode plate and are sealed to it by means of insulating seals, which also serve to support the coil.

Water flowing through the cooling coil carries off the greater part of the heat generated in the rectifier. The cooling coil provides an appreciably larger surface area for the condensation of mercury vapor as compared with the tank wall in the design of Figure 1. For a given vertical dimension, and for a mean diameter of a coil equal to the inside diameter of a cylindrical tank, the ratio of the total surface area of the coil (with closely spaced turns) to that of the tank is approximately $\pi(3.14)$. While the outside surface area of the coil is not exposed directly to the space of greatest vapor density, it is effective nevertheless, as it is also unexposed to the heat of radiation. The effectiveness of the larger surface area of the cooling coil is indicated by the test results, discussed later. Besides providing a greater surface area, the cooling coil also permits greater speed of water flow, which improves the heat transfer to the water.

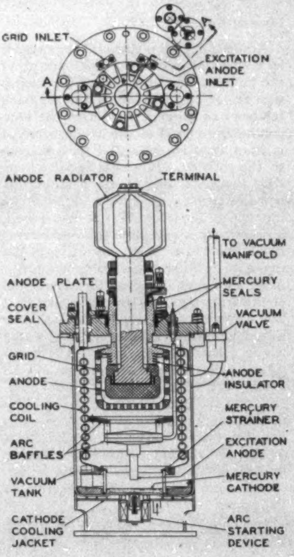


Figure 2. Cross section of new excitron design with internal cooling coil

A single graphite excitation anode is used for starting and maintaining the pilot arc at the cathode. It is supported by and insulated from the lower baffle. It is connected to an inlet stud in the anode plate by means of a stranded wire enclosed in a ceramic tube. The excitation anode carries about seven to eight amperes, supplied from a d-c source of about 35 volts. The arc is started by a jet of mercury which momentarily short-circuits the excitation anode to the cathode. A diagram of connections of the excitation circuit is shown in Figure 5.

The arc baffles, which are made of graphite, divide the rectifier tank into roughly two spaces—the anode space and the cathode space. The anode is shielded from the mercury vapor generated at the cathode, and the cathode is shielded from the heat of radiation of the anode. The anode is enclosed by a perforated basket-type control grid, which is supported by the anode insulator and is connected to an insulated stud passing through the anode plate.

The mercury cathode is enclosed by a quartz ring. Outside the quartz ring, on the bottom of the rectifier tank, is a steel ring serving as a mercury economizer. This ring with the quartz ring and the mercury strainer constitute an integral assembly, which is lowered into the tank and anchored to its sides by means of three set screws.

The bottom of the tank is water jacketed. A cylindrical section, welded to the bottom of the tank, provides the side wall of the water jacket and also serves as the base for the tank. Two cutouts in this cylinder provide access to the ignition coil. Two steel plates welded to the cylinder are used for bolting the tank to the frame and for conduction of the current from the tank to the positive connection of the unit. The tank is connected to the vacuum pumping system of the unit through a bellows-type vacuum valve.

Excitation Circuit

The excitation circuit of Figure 5 shows the connections for a six-tank excitron unit. The d-c excitation power is sup-

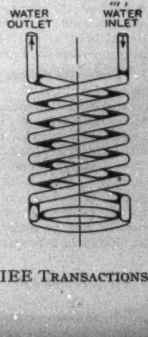


Figure 3. Construction of cooling coil

Turns are shown spread apart to illustrate the construction

Figure 4. Assembly of cooling coil and anode plate

plied from a three-phase a-c source through a three-phase transformer and a three-phase full-wave selenium-type rectifier. The ignition coils operate on alternating current. Each excitation anode is connected to the selenium rectifier through a reactor, a resistor, and the coil of an excitation relay 53.

When the three-pole supply switch is closed, the ignition coils are energized, starting the arcs between the excitation anodes and cathodes by jets of mercury. The excitation current causes relay 53 to operate and open the circuit of the ignition coil. Relay 53 remains in the operated position as long as the excitation arc is maintained. If the excitation arc should go out, relay 53 would drop down and restart the arc by energizing the ignition coil. Ignition resetting relay 31 is used to de-energize the ignition coil and permit the plunger in the ignition housing of the rectifier tank to rise, if for any reason the excitation arc is not started on the first attempt.

The purpose of the reactor in the circuit of the excitation anode is to keep the excitation arc from going out during short-time dips in the a-c supply voltage. Supplying the excitation circuit from a three-phase a-c source also contributes to the stability of the excitation arcs during system disturbances, as the majority of such disturbances are the result of single-phase faults.[6]

The grid-control system of the excitron rectifiers is practically the same as that used for multianode rectifiers, and has been described in former papers.[7]

Excitron Units

In Figure 6 is shown a 12-tank excitron unit, rated at 5,000 amperes, 650 volts (direct), for electrochemical service. It consists of two rows of tanks mounted on a steel-plate base, which is provided with four insulating supports. The vacuum pumps are located in the middle. The high-vacuum pump is connected to the vacuum manifold through a cone-type vacuum valve. The vacuum manifold is provided with 12 branches, one for each tank. The tanks are bolted to the base over copper bus bars, with interposing copper spacers. By removing these spacers a tank can be lowered and disengaged from its connection to the vacuum manifold. The copper bus bars are joined together to form the positive terminal of the unit. The water pump and temperature-control equipment are mounted at the right end of the base.

The cooling-water circuits of the tanks are connected in parallel between the supply and discharge water headers, which are placed underneath the base. The water flows from the supply header to the jacket at the bottom of each tank, then through the cooling coil to the discharge header. The water connections to the cooling coils are made by means of semirigid plastic tubes and compression-type fittings. The rectifier tanks, vacuum pumps, vacuum pipes, water pump, and water headers are connected metallically to the base, and are at the potential of the positive terminal of the unit.

The auxiliary wiring of the unit is carried in a trough between the two rows of tanks. The terminal blocks for the auxiliary wiring are mounted at the left end of the base. The connections to the anodes are made on top of the anode shafts, above the anode radiators.

In Figure 7 is shown a six-tank excitron unit. In the design of the excitron units an effort was made to reduce the usage of critical materials to a practical minimum. Copper is used only for electric conductors.

A six-tank experimental unit of this design was in operation at the Alcoa, Tenn., plant of the Aluminum Company of America for about seven months at loads up to 3,200 amperes. A view of this unit is shown in Figure 8. Many commercial units have since been manufactured and put into operation, aggregating over 400 tanks and including 26 5,000-ampere 650-volt units supplying power to aluminum-reduction potlines.

Tests

Development of Rectifiers

Because of the many variables involved and the limited knowledge of their effects, the design of a new type of mercury-arc rectifier is a development project involving the following steps:

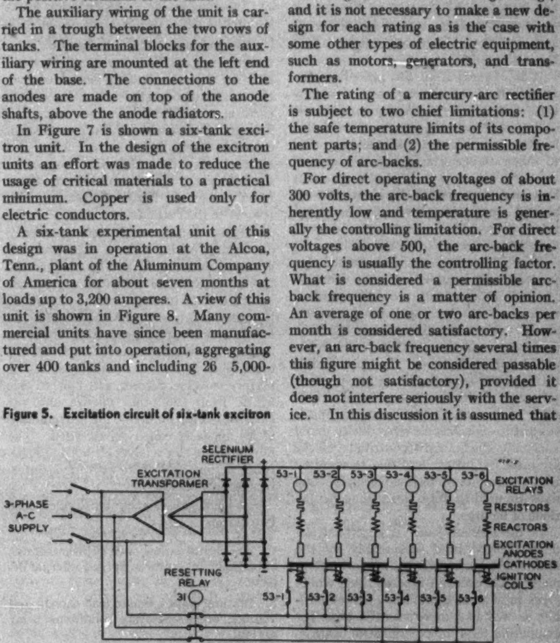
1. Experimental design, incorporating elements and materials of existing designs and introducing new elements which in the judgment of the designer would accomplish the desired results.
2. Experimental testing on the rectifier manufactured according to the experimental design. This involves load tests under various operating conditions, measurement of the critical quantities such as temperatures and arc drop, and general observation of the behavior of the machine under test.
3. Changes in the design to correct faults that have shown up during the load tests, followed by more load tests. This may be repeated many times, until the operation is considered satisfactory.
4. Final design for manufacture.

While this appears to be a laborious procedure, it should be considered that one rectifier size can be used to cover a wide range of current and voltage ratings, and it is not necessary to make a new design for each rating as is the case with some other types of electric equipment, such as motors, generators, and transformers.

The rating of a mercury-arc rectifier is subject to two chief limitations: (1) the safe temperature limits of its component parts; and (2) the permissible frequency of arc-backs.

For direct operating voltages of about 300 volts, the arc-back frequency is inherently low and temperature is generally the controlling limitation. For direct voltages above 500, the arc-back frequency is usually the controlling factor. What is considered a permissible arc-back frequency is a matter of opinion. An average of one or two arc-backs per month is considered satisfactory. However, an arc-back frequency several times this figure might be considered passable (though not satisfactory), provided it does not interfere seriously with the service. In this discussion it is assumed that

Figure 5. Excitation circuit of six-tank excitron



the rectifier is in good condition and operating in normal service. With proper protection, an arc-back does not cause any damage to the equipment, and the rectifier can be returned to service immediately.

It has been the general experience of operating companies having a number of rectifiers in one station, of identical design and operating under the same conditions, that there is likely to be a wide divergence in their arc-back performance. One may have several arc-backs a month, while another may have no arc-backs for a year or longer. There is undoubtedly a reason for this divergence, but it cannot be determined because of the many factors relating to materials and workmanship which might affect the performance.

It is obvious from the preceding discussion that the development of a new type of rectifier, or a radical change in the design of an existing type, would involve lengthy experimental load tests in order to judge its performance on the basis of arc-back frequency. It is also apparent that such tests should be conducted on several rectifiers of the same design, to rule out possible discrepancies due to defects in material or workmanship.

Efforts have been made to shorten this procedure by establishing some criterion based on shorter load runs. One method used is to increase the loading progressively until a relatively high arc-back frequency is obtained.[8] By this accelerated-loading method, the permissible rating could be approximated by plotting the arc-back frequency against load current. This method is still lengthy and is subject to considerable error due to many variables which might contribute to the arc-back frequency, and due to the fact that the results of tests covering several days are used to draw conclusions for periods of months.

In the present paper is described another method used successfully by the author and his associates in the development of the excitron-type rectifier, which gives promise of establishing a more definite as well as a more scientific basis for evaluating tests on experimental designs. It is based on the measurement of the inverse (ion) current to an anode during its nonconducting period in the cycle. While this method is not new basically,[9] it was used here for testing rectifiers in regular service on a commercial load. This method also might be used for determining the relative susceptibility of a rectifier to arc-backs under different operating conditions and possibly for judging the relative merits of different types of rectifiers under the same operating conditions. Before the test circuit and test results are described, the underlying theoretical considerations are discussed.

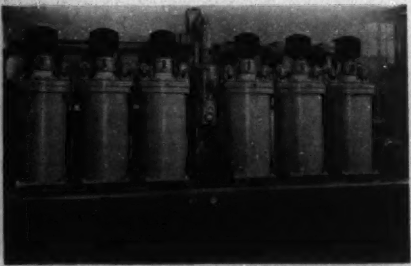


Figure 6. Twelve-tank excitron unit rated at 5,000 amperes, 650 volts direct current

CAUSES OF ARC-BACKS

The unidirectional conduction in a mercury-arc rectifier is dependent upon the fact that the cathode generates electrons, at cathode spots on the mercury surface, while the anode does not. The flow of current from the anode consists of electrons drawn to its surface when it is at a positive potential with respect to the cathode. The flow of current stops when the anode becomes negative with respect to the cathode.

When a rectifier is in operation each anode fires during part of each cycle for a period determined by the characteristics of the rectifier transformer. At the completion of its firing period the anode becomes negative to the cathode. If the anode should start generating electrons during its negative period, due to the establishment of a cathode spot on its surface, reverse current will flow to this anode from the other anodes and the cathode. This failure of the valve action of an anode is called an arc-back. The faulty anode can recover its normal rectifying action only if its current is interrupted or reversed.

Arc-back causes may be divided into two classes:

1. Accidental causes, such as poor vacuum, abnormal temperatures, surges, internal defects.

2. Natural causes, due to load current and voltage, other operating conditions being normal.

Only the latter causes are considered here, although the dividing line between accidental and natural causes is not too definite, and the mechanism of initiation of arc-backs may be similar for both types.

When an anode is carrying current, the mercury vapor in the arc path becomes ionized, the ion density increasing with the current and the density of mercury vapor. The positive ions neutralize the negative space charge of the electrons, making possible the relatively low arc drop and high efficiency of a mercury-arc rectifier.

At the conclusion of the firing period, when the anode becomes negative to the cathode, the positive ions remaining in the vicinity of the anode are drawn toward its surface, producing a small negative anode current of the order of milliamperes which is called the inverse current. The inverse current reaches a peak at the instant the anode potential reverses, then tapers off to practically zero in a short time (see Figure 13). The higher the residual ion density and the steeper the reversal of the anode-to-cathode voltage, the higher is the peak value of the inverse current.

It is the generally accepted theory that the ion current to an anode at the conclusion of its firing period is the major cause of arc-backs in rectifiers operating at voltages now in general use.[9-11] According to this theory, a high concentration of ions at a point of the anode surface can produce a sufficiently high voltage gradient to initiate a cathode spot. According to one variation of this theory, the high voltage gradient is produced by the accumulation of positive ions on an insulating particle on the surface of the anode.[11] The random nature of arc-backs is believed to be due to the random variation in the distribution of ion density at the anode.

The following circumstantial evidence can be offered as corroboration of the influence of the inverse current as a cause of arc-backs:

1. Most arc-backs occur at the conclusion of the forward firing period of an anode, when the inverse current is at a maximum.

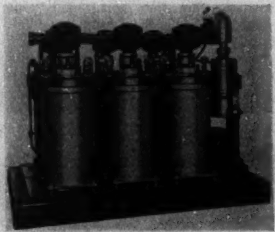


Figure 7. Six-tank excitron unit

2. The frequency of arc-backs increases as the load current and the operating voltage are increased. The increase of the current and voltage also increases the inverse current.

3. The frequency of arc-backs increases if the direct voltage of a rectifier is reduced by retarding the firing of the anodes through grid or firing control. Retardation of the firing of the anodes also results in a higher inverse current, due to the steeper reversal of the anode-to-cathode voltage at the conclusion of the anode firing period.

Further evidence of the relationship between the inverse current and arc-backs is brought out in the discussion of the experimental test results on the excitron rectifiers.

Although the *average* density of the inverse current on the surface of the anode is not sufficient to initiate a cathode spot, it is obvious that the higher the average value of this current density the greater is the probability of reaching a critical value of current density at some sensitive point on the anode, resulting from unequal distribution of ion density. The total inverse current of an anode therefore may be a criterion of its susceptibility to arc-backs. This theoretical background formed the basis of the experimental test method used in the development of the excitron.

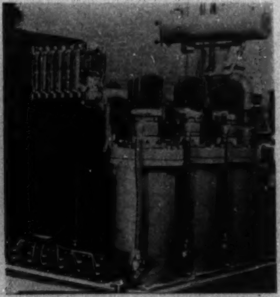


Figure 8. Experimental six-tank excitron unit

CURRENT AND VOLTAGE WAVE SHAPE

The secondary side of the rectifier transformer used in the tests had a six-phase connection with interphase transformer, which is the most generally used rectifier-transformer connection. The theoretical wave shapes of anode current and anode-to-cathode voltage for this transformer connection are shown in Figure 9. In Figure 9a are shown the wave shapes for normal firing of the anodes (no retardation). In Figure 9b are shown the wave shapes when the firing of the anodes is retarded by an angle α, for the purpose of reducing the direct output voltage. The anode current is shown with a flat top, based on the assumption that the d-c circuit has a large amount of inductance.

With normal firing of the anodes, as represented in Figure 9a, each anode starts firing at the point of intersection of its phase voltage with that of the preceding anode in the three-phase group on one side of the interphase transformer (see Figure 10). The two anodes then fire simultaneously until the current is commutated between them by the difference of their phase voltages, during a commutation angle, or angle of overlap, u. The angle u is a function of the current, the voltage difference between the phases, and the commutating reactance of the transformer. This accounts for the gradual rise and fall of the anode current. The total firing period of an anode is equal to 120 degrees$+u$.

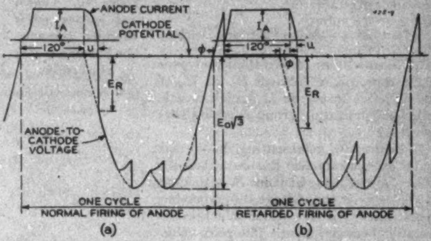


Figure 9. Theoretical wave shape of anode current and anode-to-cathode voltage

During its firing period, the voltage between the anode and cathode is positive and equal to the arc drop. At the completion of the firing, the voltage between anode and cathode reverses to a value E_R, then follows a wave shape made up of portions of sine waves. The saw-tooth distortion in this voltage wave is due to the effect of the interphase transformer and the inductive effect of the opposite phase winding, which is connected to the other end of the interphase transformer but is located on the same leg of the rectifier transformer. It is assumed here that the two windings (for example, phases 3 and 6 of Figure 12) are closely coupled and that the reactive drop in one winding during its commutation period is transmitted to the other winding through the linkage with the leakage fluxes. The dotted portions of the anode-to-cathode voltage show its wave shape if the angle of overlap is zero, the condition approached at very light load.

In Figure 9b, with retarded firing of the anodes, an anode continues to fire past the intersection point of the phase voltages until the succeeding anode is permitted to fire by the grid or firing control. The current then is commutated between them during an angle of overlap u. Before firing, the anode-to-cathode voltage becomes positive, following the rising slope of a sine wave, during the angle of retardation α. When the anode starts firing the voltage drops to the value of the arc drop. At the conclusion of the firing period the anode-to-cathode voltage reverses to the value E_R. It should be noted here that with retarded firing of the anodes the rise and fall of the anode current during the commutation periods are steeper, and the value of the reverse voltage E_R at the conclusion of the firing period is greater, as compared with the normal firing of the anode in Figure 9a.

The effects of the rate of fall of the anode current and the magnitude of the voltage E_R on the peak value of the inverse current at the conclusion of the anode firing can be readily deduced. As the anode current declines during the commutation period, some deionization (neutralization of positive ions) takes place in the vicinity of the anode. (It may be more correct to say that the rate at which ions are being neutralized exceeds the rate of generation of new ions.) The steeper the decline of the anode current during the commutation period, that is, the shorter the angle of overlap u for a given value of anode current, the less time is available for deionization and the greater will be the density of residual ions and the peak value of inverse current. This effect is dependent upon the ion density and the spacing between deionizing surfaces. At low currents and with close spacing between the anode and grid this effect may be appreciable. With large spacing between the anode and grid the effect is smaller.

The magnitude of the voltage E_R has a direct effect on the peak value of the inverse current. This voltage, which appears between the anode and the cathode upon extinction of the arc, might be termed the "recovery" voltage, corresponding to the transient recovery voltage of a circuit breaker.

QUANTITATIVE RELATIONS

In view of their influence on the inverse current, the theoretical relationships of the recovery voltage E_R, the angle of overlap u, the angle of retardation α, and the load are derived for a rectifier circuit with a rectifier transformer having a six-phase connection with interphase transformer. The following symbols are used in the derivations:

I = total direct current
$I_C = I/2$ = amplitude of anode current
I_s = peak value of commutating current
I_p = primary line current
I_{pf} = rated primary line current
E = phase-to-neutral voltage of rectifier transformer (rms value)
$E_o = E\sqrt{2}$ = peak value of phase-to-neutral voltage
X = commutating reactance per phase, in ohms
X_F = per cent commutating reactance, expressed as a fraction
u = angle of overlap
α = angle of retardation
E_R = recovery voltage
F = load factor = load current/rated current

In Figure 10 are shown the sine waves of the three phase voltages E_1, E_3, E_5, of phases 1–3–5 in Figure 12, which constitute a commutating group carrying current I_C.

In Figure 10a, representing the normal firing of the anodes (no retardation), phase 3 starts firing at point *S*. Voltage $E_{31} = E_3 - E_1$ is the voltage difference between phases 3 and 1, which effects commutation between them. The peak value of this voltage is

$$E_o\sqrt{3} = E\sqrt{6} \qquad (1)$$

If the terminals of the two phases remained joined, and the losses in the transformer and rectifier are neglected, the current that would be produced by this voltage is represented by the dotted curve I_{31}, which lags behind the voltage by 90 degrees and has a peak value I_s. The current is equal to the voltage E_{31} divided by the reactance of the two phases. Therefore

$$I_s = E\sqrt{6}/2X \qquad (2)$$

Actually phases 1 and 3 are joined by the rectifying arc only during the angle of overlap *u*, until the current in phase 3 is built up to its full value I_C. The actual commutating current flowing between phases 3 and 1 is the portion of I_{31} shown in full in Figure 10a.

Similarly the commutation between phases 3 and 5, starting at point *T*, is effected by voltage $E_{35} = E_3 - E_5$, which produces the commutating current forming a portion of current sine wave I_{35}. These portions of the current waves constitute the initial and final parts of the anode current in Figure 9a.

By inspection it can be seen that

$$I_C = I_s - I_s \cos u = I_s(1 - \cos u) \qquad (3)$$

In Figure 10b is represented the condition in which the firing of the anodes is retarded by an angle α. The starting point *S* of phase 3 is delayed by the angle α beyond the point of intersection of its voltage wave with that of phase 1. The commutating voltage and current waves E_{31} and I_{31} are the same as in section (a) of this figure. However, the actual flow of commutating current takes place along a portion of the wave I_{31} shown in full, displaced by the angle α beyond its peak. Similarly, the commutation between phases 3 and 5 takes place along a portion of the wave I_{35}. These portions of the current waves constitute the initial and final parts of the anode current in Figure 9b.

By inspection it can be seen that

$$I_C = I_s \cos\alpha - I_s \cos(\alpha + u)$$
$$I_C/I_s = \cos\alpha - \cos(\alpha + u) \qquad (4)$$

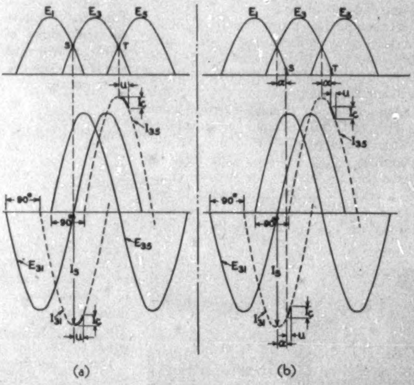


Figure 10. Current and voltage waves effecting commutation between anodes of a rectifier

(a)—For normal firing of anodes

(b)—For retarded firing of anodes

If $I/2$ is substituted for I_C, and $E\sqrt{6}/2X$ from equation 2 for I_s,

$$I_C/I_s = IX/(E\sqrt{6}) \qquad (5)$$

It is desirable to express this relationship in terms of rated load and per cent commutating reactance, which are known quantities. If a one-to-one ratio between the primary and secondary phase-to-neutral voltages of the rectifier transformer is assumed, the primary voltage and commutating reactance are $E_p = E$ and $X_p = X$. The primary line current[13] (disregarding the correction factor for overlap)

$$I_p = I/\sqrt{6} \qquad (6)$$

If these expressions are substituted in equation 5,

$$I_C/I_s = I_pX_p/E_p \qquad (7)$$

The primary line current, I_p, at any load may be expressed in terms of rated full-load current, I_{pf}, by a load factor *F*.

$$I_p = FI_{pf} \qquad (8)$$

If this expression is substituted in equation 7,

$$I_C/I_s = FI_{pf}X_p/E_p = FX_F \qquad (9)$$

The factor $X_F = I_{pf}X_p/E_p$ is the per cent commutating reactance of the transformer expressed as a fraction.

Substituting from equation 9 in equation 4 gives

$$I_C/I_s = FX_F = \cos\alpha - \cos(\alpha + u) \qquad (10)$$

This expression makes it possible to calculate the angle of overlap *u*, for a given angle of retardation α, in terms of the load and reactance.

From Figure 9 it can be readily seen that

$$E_R/(E_o\sqrt{3}) = \sin(\alpha + u)$$
$$E_R/E_o = \sqrt{3}\sin(\alpha + u) \qquad (11)$$

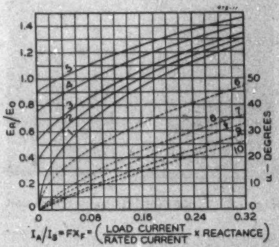
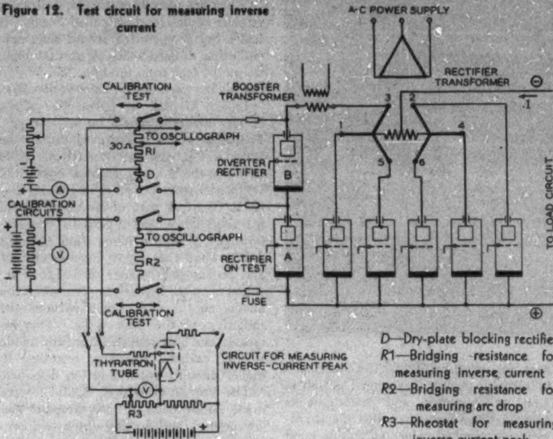


Figure 11. Curves of recovery voltage and angle of overlap for different values of direct-voltage reduction by phase control

Per Cent Reduction of Direct Voltage $(1-\cos\alpha)\times100$	E_R/E_o	u
0	Curve 1	Curve 6
2.5	Curve 2	Curve 7
5	Curve 3	Curve 8
10	Curve 4	Curve 9
15	Curve 5	Curve 10

The ratio E_R/E_o of recovery voltage to peak value of the secondary phase voltage can be computed in terms of the load and reactance with the aid of equation 10. This ratio and the angle of overlap

Figure 12. Test circuit for measuring inverse current



D—Dry-plate blocking rectifier

R1—Bridging resistance for measuring inverse current

R2—Bridging resistance for measuring arc drop

R3—Rheostat for measuring inverse-current peak

are shown as a function of FX_r by the curves of Figure 11, for several values of the angle of retardation (phase-control angle) α corresponding to reduction of the direct voltage by 0, 2.5, 5, 10, and 15 per cent.

Test Circuit and Procedure

The tests were made on a rectifier unit operating at about 625 volts (direct) and supplying a commercial load in parallel with a group of other rectifier units. The primary winding of the rectifier transformer was connected to a 13,200-volt three-phase 60-cycle a-c system.

The test circuit used for measuring the inverse current is shown in Figure 12. Six single-anode rectifiers are connected to the six-phase secondary winding of the rectifier transformer. The rectifier *A* on test is connected to one phase of the transformer in series with another rectifier *B*, serving as a current diverter. A single-phase booster transformer with a low-voltage secondary winding is connected in series with the two rectifiers to compensate for the arc drop of rectifier *B*.

The diverter rectifier is used to permit measuring the inverse current, which is very small compared to the forward anode current. The forward current of rectifier *A* flows through rectifier *B* and is thus diverted from the inverse-current measuring circuit. The inverse current of rectifier *A*, or the greater part of it, flows over resistor *R*1, which is connected across rectifier *B*. A selenium dry-plate rectifier *D* is connected in series with resistor *R*1 to block the flow of positive current from the arc drop of rectifier *B* during the conduction period, which otherwise would limit the deflection of the inverse-current trace on the oscillograms. One galvanometer of a magnetic oscillograph is connected to a section of resistor *R*1.

Because of the inertia of its vibrators, a magnetic oscillograph could not be depended upon to record the instantaneous peak of the inverse current. A thyratron tube connected as shown in Figure 12 was used for measuring the inverse-current peaks. The tube is connected in series with a resistor to a d-c source, shown as a battery. The grid of the tube is connected to receive positive potential from resistor *R*1 in series with an adjustable negative bias from rheostat *R*3. To obtain a measurement, the bias voltage is lowered gradually until the tube fires. The negative bias voltage, corrected for the grid-voltage characteristic of the tube, is a measure of the peak voltage across resistor *R*1 and, consequently, of the peak inverse current.

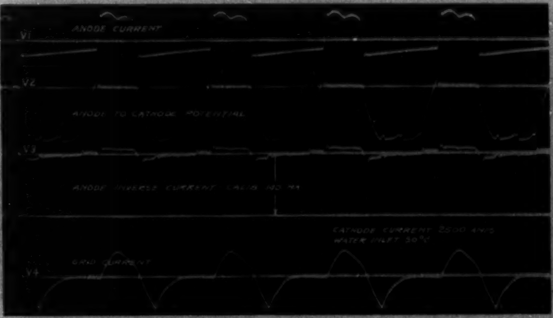
A resistor *R*2 with oscillograph connections is used for measuring the arc drop of the rectifier on test. Resistors *R*1 and *R*2 are provided with two-pole double-throw switches and calibrating circuits. Only one of these switches was in the test position at a time, and separate oscillograms were taken of the inverse current and arc drop.

Typical oscillograms taken during the tests are shown in Figures 13 and 14. In Figure 13 the inverse current is shown by trace *V*3. The inverse current is in the direction below the zero line, and the magnitude is determined from the calibration line. Trace *V*1 shows the anode current, obtained by means of a current transformer. Trace *V*2 is the anode-to-cathode voltage, taken from the anode of rectifier *B* to the cathode of rectifier *A*. Trace *V*4 shows the grid current. As can be seen from the positive portion of the anode-to-cathode voltage before the firing of the anode, the rectifier operated with retarded firing of the anodes, by phase (grid) control.

In Figure 14 the arc-drop is shown by trace *V*3. The portion *h*, appearing after the anode ceased firing, is produced by ion current which is collected by the anode of rectifier *A* and flows to the cathode over resistor *R*2. The grid arc drop, shown by trace *V*4, was obtained by the use of an amplifier, which made it possible to use a high-resistance circuit between the grid and cathode and to suppress the negative part of the grid-to-cathode voltage. An alternating voltage, recorded by *V*5, was used for calibration.

In order to reduce to a minimum the proportion of inverse current flowing through the diverter rectifier *B*, this rectifier was operated with a cooling-water temperature of about 25 degrees centigrade to maintain a low density of ionization at its anode. The inverse current by-passed through rectifier *B* also was limited by the low reverse voltage between its anode and cathode, which is equal to the product of the resistance *R*1 and the inverse current through it.

Figure 13. Oscillogram of inverse current



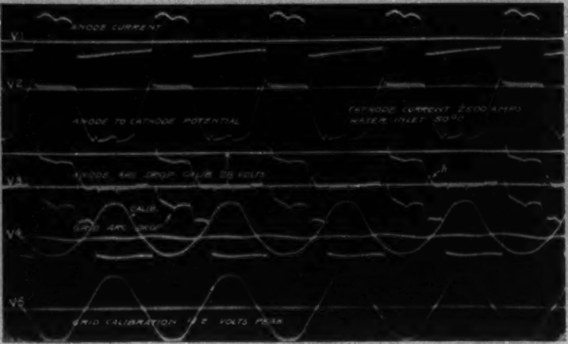


Figure 14. Oscillogram of arc drop

The by-passed current could be an important item, nevertheless, as is indicated by the test results.

The tests were made at several values of load current on the rectifier and several different temperatures of the cooling water. The rectifier was operated at each load and temperature setting sufficiently long to obtain stable conditions before making measurements. During some of the tests arc-backs occurred on the rectifier tank being tested while the resistor *R*1 was connected across rectifier *B*. The arc-back current was limited by the resistor and extinguished itself when the anode became positive. While the rectifier was on load between measurements, the rheostat *R*3 was set for a high negative bias on the grid of the thyratron tube so that the tube served as an arc-back indicator.

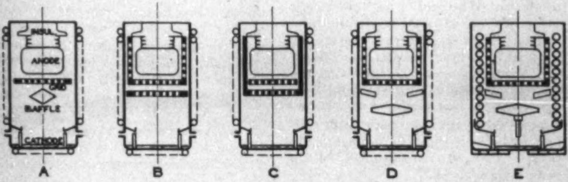


Figure 15. Schematic construction of experimental excitron designs tested

TEST RESULTS

The schematic constructions of some of the excitron designs tested are shown in Figure 15. The designs *A*, *B*, *C*, and *D* had insulated cathodes and soldered-on copper cooling coils. The chief difference between these designs was the type and arrangement of grids and baffles. Design *E* had an internal cooling coil and was similar to that shown in Figure 2. The grid and baffles are similar to design *D*.

A wide range of values of inverse current was measured during the tests. Oscillographic peak values ranged as high as 1,500 milliamperes, and instantaneous peak readings measured with the thyratron tube reached values as high as 3,500 milliamperes. Variations were observed in the oscillographic and instantaneous peak values for a given design and under the same external operating conditions.

As the tests were made at different values of load current and with a variable amount of firing retardation by grid control, the anode-to-cathode recovery voltage varied, which in turn affected the inverse-current peak. It was therefore not convenient to use the inverse current directly for making comparisons. The ratio of the recovery voltage to inverse current peak was used for this purpose. This ratio was termed the inverse-current resistance factor R_i. The higher the inverse current for a given value of recovery voltage, the lower is the value of R_i.

In Table I are given the values of R_i obtained for the rectifier designs shown in Figure 15 for several different load currents and cooling-water temperatures. The values of R_i determined from the oscillographic measurements of the inverse current, as well as those obtained from the measurements with the thyratron tube, are listed.

When the tests were started, a diverter rectifier of design *A* was used because it had a better operating record than several other designs available at that time. As can be seen from the table, this design has higher values of R_i than design *B*.

Design *C* has the lowest R_i values. It was also the worst in performance. Frequent arc-backs occurred during the tests, and it was not deemed advisable to keep the unit in regular operation. It differed from design *B* mainly in that the cylindrical part of its anode shield was solid, while that of design *B* was perforated. It was evident from the test results that the higher inverse current and poorer performance of design *C* resulted from the higher temperature of the anode, as the solid shield reduced the radiation to the tank walls. This increased the heat radiated from the anode to the cathode mercury, resulting in higher mercury-vapor and ion densities.

The tests on designs *A*, *B*, and *C* were made up to loads of 2,000 amperes (on six anodes) and cooling-water temperatures of 45 degrees centigrade, as their performance at higher loads and temperatures was unsatisfactory.

The test results on these designs led to design *D*, with baffles between the anode and cathode which shielded the cathode from the heat radiated by the anode and also shielded the anode from the mercury vapor of the cathode. There was a decided improvement in the performance of this design over any of the designs tested previously, although the same tanks and anodes were used. It was possible to operate the rectifiers of this design at loads of 2,500 amperes and cooling-water temperatures of 55 degrees centigrade.

The values of inverse current measured on design *D* were much lower than on design *A*, as indicated by the R_i figures shown in parentheses in Table I. These figures are erratic and show some higher R_i values at increased loads. This resulted from the use of a diverter rectifier of design *A*, which had a relatively higher ion density than design *D* and by-passed too high a proportion of its inverse current. The reduction in the inverse current on design *D* was therefore not as much as indicated by the measurements.

It was apparent from these results that a diverter rectifier with higher R_i characteristics would have to be used for testing rectifiers of design *D*, and others that might follow, in order to obtain data that could be used for further comparisons. A diverter rectifier of design *D* was used in the succeeding tests. The test results on rectifier designs *D* and *E*, using the new diverter, are given in Table I. No direct comparison is possible between these values of R_i and those of designs *A*, *B*, and *C*.

As was stated previously, the essential difference between designs *E* and *D*, aside from the elimination of the cathode-to-tank insulation, is that design *E* has an internal cooling coil which provides considerably more surface for condensation of mercury as compared to the tank wall of design *D*. The R_t values of design *E* average about 1.5 and 2 times those of design *D* for the two methods of measurement. (It appears from the test results that a diverter rectifier with still higher R_t characteristics should be used for testing design *E*.) The performance of design *E* was also superior. It could operate at higher loads and higher cooling-water temperatures. The six-tank experimental unit shown in Figure 8 had one arc-back during its seven months' operation at continuous loads up to 3,200 amperes with occasional higher loads for short periods.

Effect of Excitation Arc

One question in connection with the development of the excitron was the contribution of the excitation arc to the inverse current of the anode, which might be an indication of its possible effect on arc-backs. Measurements were made on several excitron designs, with the anode at a negative voltage to the cathode, with the excitation arc on, and with different values of positive and negative voltages on the grid. With an anode voltage of about −270 volts and a grid voltage of about +90 volts, a negative current of approximately three milliamperes was measured on an excitron similar to design *B*, Figure 15, but without the lower grid. The measurement was made shortly after the rectifier carried load. Lower readings were obtained on other designs tested, being only a fraction of a milliampere on some.

In Figure 16 is shown an oscillogram of the inverse current taken on the aforementioned excitron, in which the excitation arc was interrupted while the anode was firing. The trace of the excitation current is shown at the bottom of the oscillogram. If the excitation arc had any effect on the inverse current, while the anode-to-cathode voltage was negative, it was too small to be noticeable. The higher inverse-current peak, after the excitation was interrupted, might have been a coincidence, as it did not occur on other oscillograms taken during this test. The trace of the grid current included the current through a resistor which was connected between the grid and cathode. This accounts for current recorded on this trace after the arc was extinguished.

Discussion of Test Method

The discussion of the test results has brought out the usefulness of the test method described in this paper for evaluating the performance of rectifiers. This test method is not a substitute for load runs but a valuable aid in predicting performance and determining the effects of design changes. Results can be obtained from tests at normal loads of relatively short duration. Load runs of longer duration are still desirable to determine how a unit stands up in service.

Reasonable care has to be exercised in the use of this method. It can be used only for the purpose of making comparisons between different rectifiers under approximately the same operating conditions, or for the same rectifier under different operating conditions. Tests should be made on more than one rectifier of a given design in order to rule out possible foreign influences, such as bad vacuum or dirty mercury-condensing surfaces, in individual tanks. A diverter rectifier with low inverse-current characteristics should be used in order to reduce to a minimum the inverse current by-passed through it. When measuring the inverse-current peaks with the thyratron tube a number of readings should be taken and averaged, as there are likely to be variations in the readings.

No precision results are to be expected, and too much weight should not be given to small differences. The results should be interpreted on an average, over-all basis. When a rectifier is on load there are likely to be variations in the internal conditions, such as mercury-vapor density, besides variations in the external load current and system voltage, which preclude precision results.

The use of resistance factor R_t does not necessarily imply a direct proportionality between the inverse current and recovery voltage for a given load

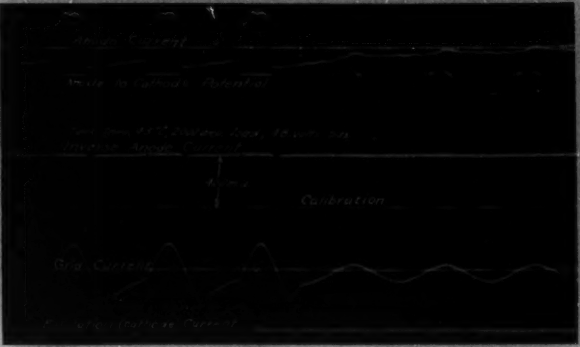


Figure 16. Oscillogram of inverse current with interrupted excitation arc

Table I. Inverse-Current Resistance Factor R_t

D-C Load Current on Six Anodes, Amperes	Cooling-Water Inlet Temp (C)	Excitron Design on Test (See Figure 15): With Excitron Design A as Diverter Rectifier: A ×10³	B ×10³	C ×10³	D ×10³	With Design D as Diverter: D ×10³	E ×10³	
1,000	35	6.9	3.6	1.5	(24.5)	5.5	9.3	Measured with magnetic oscillograph
	45	4.1	2.9	0.9	(19.7)	5.0	7.1	
	55					2.7	2.3	
1,500	35	6.1	2.5	0.8	(30.5)	5.4	8.4	
	45	2.8	2.1	0.5	(31.5)	5.1	7.4	
	55					2.6	2.7	
2,000	35	4.4	1.8	0.4	(33.6)	5.0	9.4	
	45	1.8	1.3	0.3	(25.2)	4.6	8.4	
	55					2.3	2.7	
2,500	35					6.6	10.2	
	45					4.7	9.1	
	55					2.3	2.9	
1,000	35	0.79	0.56	0.34	(9.5)	1.06	2.2	Measured with thyratron tube
	45	0.72	0.43	0.25	(6.0)	0.89	1.6	
	55					0.65	0.71	
1,500	35	0.58	0.41	0.22	(7.8)	0.81	1.8	
	45	0.42	0.32	0.18	(6.3)	0.76	1.7	
	55					0.55	0.94	
2,000	35	0.55	0.30	0.15	(19.3)	0.65	1.8	
	45	0.36	0.23	0.13	(15.5)	0.78	1.9	
	55					0.5	1.4	
2,500	35					0.76	1.4	
	45					0.62	1.4	
	55					0.44	0.78	

current and cooling-water temperature, although an approximately linear relationship was found in a number of instances checked. This relationship depends on the distribution of potential and speed of deionization in the vicinity of the anode. However, R_t still can be used as a factor of comparison if the variations of the recovery voltage are not too great. If possible, these variations should be kept to a minimum. Where large variations of the recovery voltage are unavoidable, due to the use of a variable amount of grid control for maintaining a constant load current during the test, the relationship between the recovery voltage and the inverse current can be determined by making a number of measurements at different values of recovery voltage. The results then can be related to one value of recovery voltage for purposes of comparison.

Arc Drop

The arc-drop voltage of a rectifier is one of its important performance characteristics. In the design and development of a rectifier it is usually necessary to make a compromise between the arc drop and the arc-back performance. A rectifier with close spacing between the anode and cathode and without any baffles between them would have a low arc drop, but could not be operated at the usual rectifier voltages and temperatures on account of the excessive number of arc-backs. The aim of the designer is to produce a rectifier with a good arc-back performance without too much sacrifice in arc drop. Most engineers who have had operating experience with rectifiers are likely to give greater weight to the arc-back performance and continuity of service, even if it means some sacrifice of efficiency, provided the sacrifice is not too great.

The difference in the values of arc drop measured during the tests on the experimental excitron designs shown in Figure 15 was not too great. An arc drop of about 17 volts at 2,000 amperes (on six anodes) was measured on rectifier design *A*. On design *E* the measured arc drop was about 20 volts at 2,000 amperes and about 21 volts at 2,500 amperes. On design *C*, which had the worst arc-back performance, an arc drop of about 19 volts was measured at 2,000 amperes. The results indicate that while some increase in arc drop is to be expected to obtain better operating performance, a rectifier with a higher arc drop is not necessarily a better-operating rectifier. A good balance has to be struck between the elements affecting these performance characteristics.

Conclusion

This paper has outlined the practical and theoretical problems involved in the design and development of rectifiers and the method of approach used in their solution. It is hoped that this will give engineers interested in rectifiers a better understanding of the factors influencing their performance.

The test method used in the development of the excitron, and similar test methods, should help to bridge the gap between the theory and practice of rectifier design. As more knowledge is gained of the elements related to the performance of a rectifier, the design of rectifiers will become more rational and less empirical, and much of the cut-and-try procedure now used would be eliminated.

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Damping Cylinders for Aircraft Gyroscopes

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Synopsis: The operation of air-damping cylinders in restraining oscillation of a pendulum system is studied by experimental and analytical methods. On account of fundamental differences which are shown to exist between such cylinders and other damping devices the design of cylinders must follow certain dimensional requirements which are dictated by the dynamics of the whole oscillating system. These considerations determine the minimum size of cylinder which can be used in a given application and also the piston clearance which gives maximum damping effect.

EVERY mechanical system in which there is mass and spring force or its equivalent has a tendency to oscillate. If such oscillation would interfere with proper operation of the system, it must be checked by some sort of damping. In a broad sense, any method of dissipating energy to bring about successive reductions in the amplitude of oscillation is called damping. Three types may be recognized: electromagnetic or eddy-current, liquid friction, or gaseous friction. The type to be used in any application depends on the requirements of the individual problem. Thus, most electrical indicating instruments have electromagnetic damping, while automobile suspensions generally use liquid friction (hydraulic shock absorbers). Gaseous friction is suitable for damping indicating instruments but requires close tolerances on dimensions.

The problem considered below deals with the pendulum system controlling the gyroscope in an artificial horizon indicator, Figure 1. Such a pendulum is hung by gimbals giving it freedom of motion about two axes and since the bearings are as frictionless as possible, it can oscillate without restraint about both axes. As this cannot be tolerated in operation, some form of damping must be provided. Moreover, three limitations are imposed on the choice of a system:

1. Space is very limited.
2. Magnetic damping is undesirable and quite heavy.
3. No static friction can be permitted as this would cause error in the indication.

A practical means of meeting these requirements was found to be the use of small cylinders with close-fitting pistons and one end closed. One cylinder is associated with each axis of support of the pendulum system. Its connecting link is attached to the part which it restrains at a distance from the pivot point such that the piston travels from one end of the cylinder to the other during motion of the part over its free range. It is understood in future discussion that the length of each cylinder is determined by this consideration. The two cylinders appear in Figure 1. The horizontal cylinder link is attached to an upright extension of the pendulum arm. The vertical cylinder is attached to a point directly below it on the erecting gimbal.

One difference in performance exists between air-cylinder damping and that provided by liquid friction or magnetic means. This difference is considered below in some detail from both the physical and mathematical viewpoint. It is brought out that while a high damping factor is obtained in a small space, there are definite limitations to the effectiveness of this damping and these are particularly significant in a device which must operate over a wide range of atmospheric pressure. On account of the high altitude at which aircraft may fly, the gyrohorizon encounters such a variation in pressure.

Operation of Damping Cylinders

Before the air-damping cylinder is considered in its relation to the present problem, the property which distinguishes it from other damping devices will be reviewed. This is best seen by comparing its performance under a constant applied force with that of a true damping device like a conducting vane in a magnetic field. When such a force is applied to the vane, it starts to move immediately at a velocity such that the counter force due to eddy currents equals the applied force. If the latter is removed, motion stops immediately. Now consider a damping cylinder under a similar situation. When a force is first applied, the piston is displaced through a distance sufficient to produce an equal counterforce by a pressure difference between the two ends of the piston. Thereafter, a steady-state velocity results as air leaks out around the piston. However, an initial displacement is required to set up the counter-force and when the applied force is removed, the piston will deflect back to relieve the pressure.

Oscillation of the Pendulum System

In applying damping cylinders to the pendulum system described above, it is necessary to understand the origin of the oscillations which they are required to control. These are of two sorts. First are external shocks and forces due to motion of the aircraft. These are transient in nature. Second are internal forces resulting from the interaction of the erecting magnet with the erecting disk. Under conditions of normal operation, the erecting magnet is centrally located by gravity under the rotating erecting disk. This tends to twist the magnet because of the electrical forces set up, the twisting force acting back through the pendulum bearings. There is no tendency to produce oscillation until some external force moves the magnet out of its central position. The magnet now tries to follow the rotation of the erecting disk. If the natural frequency of oscillation about both axes were equal, the magnet, if undamped, would follow a circular path, the rotational speed being about equal to the frequency of the pendulum. Where the two natural frequencies are not equal, the pendulum locus takes a more complex form, but the system still requires some damping mechanism to prevent sustained oscillation.

Action of a Damping Cylinder in an Oscillating System

When a damping cylinder using air as the damping fluid is connected to an oscillating system such as a pendulum, two effects are noticed. First, damping occurs as energy is used to overcome the friction of air passing through an orifice, either around the piston or through a special fixed or adjustable opening, and second, the frequency of oscillation of the system is altered because of the elasticity of the air in the cylinder.

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Table I

Rotational Mechanics			Electric Circuits		
Name	Symbol	Units	Name	Symbol	Units
Torque	T	Dyne centimeters	Voltage	e	Volts
Angular displacement	θ	Radians	Charge	q	Coulombs
Angular velocity	ω	Radians per second	Current	i	Amperes or coulombs per second
Moment of inertia	I	Grams centimeter^2 or dyne centimeter second^2	Inductance	L	Henrys or volt second^2 per coulomb
Rotational compliance	C_θ	Radians per dyne centimeter	Capacitance	C	Farads or coulombs per volt
Rotational damping factor	D	Dyne centimeter-seconds per radian	Resistance	R	Ohms or volt second per coulomb

The damping constant of the cylinder is dependent on air pressure to some extent, but at 14 centimeters of mercury absolute pressure it has approximately 80 per cent of its value at 76 centimeters pressure. Figure 2 shows experimental results. Damping constant is defined as force per unit velocity in steady-state linear motion. It is not synonymous with effective damping in an oscillating system, a fact which will be shown later. Fourteen centimeters of mercury absolute pressure is approximately the pressure at 40,000 feet above the earth's surface. Gyroscopic instruments are required to operate correctly at that altitude; therefore, the change in damping constant within that range is not serious.

The change in the frequency of oscillation is considerable. At absolute zero pressure the frequency is that of the pendulum without the damping cylinder. As the pressure increases, the elastic action becomes stiffer and the frequency of oscillation increases. The amount of this change depends on the pressure differences produced by oscillation of the pendulum, these pressure differences being dependent on the piston area, the length of the lever arm by which the piston is connected to the pendulum, and the initial pressure in the cylinder.

Short lever arms and small cylinder diameters produce small changes in the frequency, but a large elastic effect, due to the high pressure on the confined air with a given pendulum force. This effect may be sufficiently great in small-diameter cylinders that it is not possible even at normal atmospheric pressure, to obtain critical damping without having noticeable elastic effect. This gives the appearance of underdamping at all times unless the motion is closely observed. With underdamping, the oscillating system will pass the normal rest position several times before the oscillation ceases. When elasticity is present and the clearance between the cylinder and piston is too small, instead of passing the normal rest position it will gradually approach it and also swing back and forth at the same time. As the amount of clearance is increased, the pressure on the confined air becomes less and the elastic effect decreases. It is therefore possible to determine a point at which maximum damping is obtained with minimum springiness.

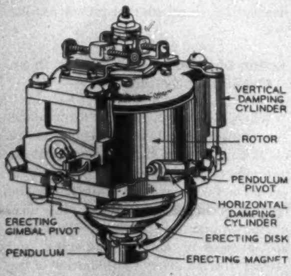

Figure 1. Gyro-horizon motor and erecting gimbal assembly

The amount of elasticity is also dependent on the absolute pressure, the "spring" becoming weaker and allowing more motion of the oscillating system as the pressure decreases. Consequently, although the damping constant varies only slightly with absolute pressure, the maximum effective damping may be considerably less than would appear to be possible if there were no elasticity.

Figures 3, 4, and 5 are tracings from oscillograms taken by a process which will be described later. They illustrate the statements which have been made above.

Each curve represents the oscillation of a pendulum which has been moved from its rest position, is released, and allowed to swing.

Figure 3*A* represents the condition where there is no damping device. Because of a small amount of mechanical and air friction, some damping is present and the length of swing becomes less at each successive swing.

If the pendulum is provided with a perfect damping means such as eddy-current damping, and this is adjusted to provide critical damping, the movement to the rest position will be accomplished in one swing, Figure 3*B*.

Another limiting condition is that in which an air-damping cylinder is used, but the piston fits the cylinder perfectly so that no air flows past the piston. The system will not return to the rest position at all, but will oscillate about a point between the starting point and rest position, Figure 3*C*. This oscillation will also die out finally, due to mechanical or air friction. Note the increase in frequency with comparison to curve *A*.

Figure 4 shows the operation of a one-fourth-inch-diameter horizontal damping cylinder, Figure 1, at normal atmospheric pressure. Curve *A* represents the motion of the pendulum when the piston fits loosely in the cylinder. Oscillations continue for some time. In curve *B*, the piston fit is optimum and nearly critical damping is obtained. Curve *C* shows the action of the system when the piston is too tight. The pendulum returns to the rest position at a rate that corresponds to overdamping (as shown by the dotted line) but at the same time oscillates about this line. In both *A* and *C*, the oscillations continue for some time after they would if a piston of optimum fit were used.

Figure 5*A* shows the performance which would result at a pressure of 14 centimeters of mercury when the piston fit is optimum for this condition of operation. Compared to 4*B*, it shows the oscillations continuing for a much longer time because the elastic effect is much more pronounced at this low pressure.

Figure 5*B* also represents a pressure of 14 centimeters of mercury, but a $^5/_{16}$-inch cylinder with a piston of optimum fit is used. In this case, oscillations die out more quickly than they did with the one-fourth-inch cylinder.

Oscillation Frequency

Changes in frequency which occur as a function of piston clearance at various pressures will be dealt with from an analytical viewpoint covered below. Results obtained in this way are shown in Figure 6. The abscissa may be considered for the moment to represent piston clearance progressing left to right from very loose to very tight. The ordinate is the natural frequency of the system about the pendulum pivots. Curve *A* corresponds to operation of the damping cylinder at normal atmospheric pressure, 76 centimeters of mercury, and curve *B* represents 14 centimeters of mercury. In the limiting case of a loose fit, the frequency of the system at either pressure is the natural frequency of the pendulum alone. As the clearance of the piston is reduced, the elasticity of the confined gas becomes more effective and causes the frequency of the system to increase, the effect being more pronounced at higher pressures.

In the gyro-horizon studied, the natural frequency of oscillation about the pendulum pivots is approximately that of a simple pendulum. When the system swings about the erecting gimbal pivots, the mass of this gimbal is added, making a compound pendulum of lower oscillation frequency. When damping cylinders are added, since the vertical damping cylinder has a longer lever arm than the horizontal cylinder, it becomes stiffer as the absolute pressure increases, more rapidly than the horizontal cylinder. Thus, the frequency of oscillation about the erecting gimbal pivots increases at a more rapid rate than about the pendulum pivots. The frequency of oscillation about the two axes, therefore, approaches the same value. At this point the oscillation of the whole erecting system is most violent. As the pressure is further increased, the oscillation frequency about the erecting gimbal pivots becomes greater than about the pendulum pivots, finally becoming about twice as great as at normal atmospheric pressure. It is therefore important either to make the point of maximum tendency to oscillate come at a pressure outside the service range or at a point where damping is sufficient to overcome it.

Test Procedure and Performance Requirements

Preliminary damping-cylinder tests were made with one-fourth-inch diameter cylinders as shown in Figure 1. The test consisted of placing the complete operating gyroscope assembly under a bell jar which could then be evacuated to any desired pressure. In making the tests, the absolute pressure was reduced to about two centimeters of mercury, and the bell jar was then shaken to start oscillation of the pendulum. The pressure was gradually raised until the oscillations stopped. This value was recorded. As a final standard of acceptance, if the oscillation stopped at an absolute pressure of 14 centimeters of mercury, the instrument was considered satisfactory. Several sizes of pistons were tried in both the vertical and horizontal cylinders.

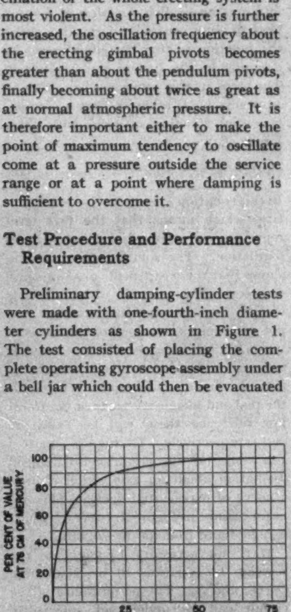

Figure 2. Damping constant of air-damping cylinders versus pressure

There was very little oscillation of the erecting gimbal, since critical damping could be obtained easily. The horizontal cylinder could not be made critically damped, but in the best case the damping was satisfactory. Figures 7 and 8 illustrate the effect of piston diameter. Note that while the horizontal-piston diameter must be very closely controlled, the vertical piston may vary over a fairly wide range if the proper horizontal piston is used.

Tests were made next using a $^5/_{16}$-inch-diameter horizontal cylinder and a similar set of curves were drawn, Figures 9 and 10. The allowable tolerance in piston diameter is now much greater and, with a piston of best size, the oscillations stop at a much lower pressure. This size of cylinder makes a satisfactory assembly both from the manufacturing and functional standpoint.

Mathematical Analysis of the Oscillating System

Discussion of oscillation and damping in the pendulum system has been largely qualitative up to this point. It has been established that a damping cylinder does not operate like a pure damping mechanism, but combines an elastic action with its damping function. From a mechanical point of view the system composed of the pendulum and damping cylinder may be considered to consist of four elements: (1) A moment of inertia, which is the pendulum; (2) A restoring torque, which is due to gravity and will be assumed proportional to deflection; (3) A damping vane or other pure damping device, connected to the pendulum by means of (4) A spring. The constant of the spring, as it affects the swinging of the pendulum, depends on the dimensions of the damping cylinder, the length of the lever arm at which it acts on the pendulum, and the prevailing air pressure. The damping constant also depends on cylinder size and lever arm. In addition it is greatly affected by piston clearance in the cylinder, an effect which provides control after other design factors are fixed. The relatively small effect of air pressure has been noted.

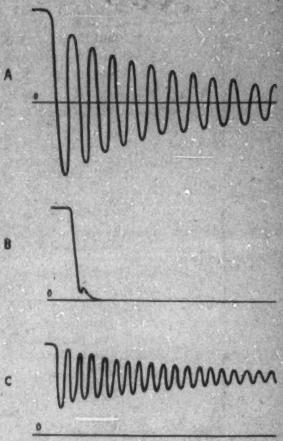

Figure 3. Oscillograms showing limiting conditions in an oscillating system

A—Minimum damping
B—Critical damping
C—Air-damping cylinder with no air flow

Method of Electrical Analogy

This system of four elements can be treated as a study in rotational mechanics, but there are certain advantages in dealing with an electrical analogy which reproduces the same conditions. This describes the phenomena in language more familiar to the electrical engineer and facilitates a comparison with other common electrical circuits of known behavior. Moreover, it makes available a ready means of experiment to supplement analytical studies.

The subject of mechanical-electrical analogies has been treated at great length,[1] but for the purposes of this problem, a definition of a few fundamental quantities in each system is sufficient. This is best shown as a parallel tabulation, Table I.

It is to be noted that basically there are only three units in each system: dyne-centimeter-second, and volt-coulomb-second. The radian in the mechanical system is a dimensionless unit. The concept of rotational compliance may not be as familiar as its reciprocal, which might be called rotational spring con-

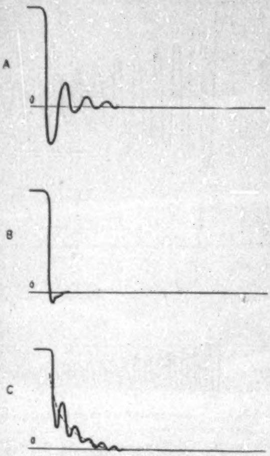

Figure 4. Oscillograms showing operation of air-damping cylinder at normal atmospheric pressure

A—Loose piston
B—Optimum piston
C—Tight piston

stant, but it is more convenient for translation of terms between the two systems.

To represent the mechanical system electrically, the pendulum with its moment of inertia and gravity restoring torque become an inductance and capacitance. The damping cylinder, with its combination of damping and spring action, may be represented by a resistance and capacitance in parallel. The complete circuit is shown in Figure 11. So far as possible, the analysis of the system will be made entirely in these electrical terms, reverting to the mechanical only where the physics of the original problem must be examined.

The performance to be expected from such a system can be predicted at least in part from a number of different methods of analysis. Before a complete treatment is started, it is worth noting that certain qualitative conclusions follow immediately on inspection of the circuit. Consider the effect of resistance on damping. Here it is useful to make a comparison with a simple series circuit of resistance, inductance, and capacitance (later referred to as *RLC* circuit). In such a circuit, damping increases continuously with resistance, passing from an oscillatory underdamped behavior through critical damping to a nonoscillating overdamped behavior. Let resistance increase in the same way in the circuit of Figure 11. At zero resistance the capacitor C_1 is short-circuited and only L and C remain. This gives undamped oscillation at the frequency determined by L and C. As R increases clearly some power is dissipated and some damping results; however, if R is made indefinitely large the circuit reduces to L, C, and C_1 all in series, which is again an undamped oscillating circuit having a new natural frequency. Evidently then, some finite value of R gives most effective damping. It will be one object of the analysis that follows to determine this value. Again consider the effect of changing C_1. If this capacitance is zero, the circuit reduces to *RLC* and damping depends on R as noted above. If C becomes very large, it approaches a short circuit on R and evidently the system could never be effectively damped regardless of the value of R.

This fact has immediate application to the case at hand and confirms conclusions already reached in terms of actual cylinders. It has been said that the damping cylinder must operate over a pressure range corresponding to high-altitude flying. This is taken to be 14 to 76 centimeters of mercury. The "springiness" of the damping cylinder due to the compressibility of air has been shown to vary directly with pressure. As pressure is reduced, the "compliance" increases and in the electrical analogy the capacitance also increases. At 14 centimeters of mercury it has, roughly, six times the value at 76 centimeters. Since the damping factor of the cylinder changes relatively little in this pressure difference the resistance in the electrical circuit does not itself change by a large factor, but the large change in capacitance greatly reduces the effectiveness it can have in damping oscillation. This will appear numerically from results to follow.

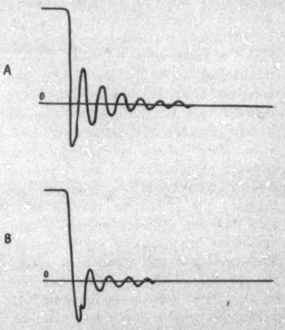

Figure 5. Oscillograms showing operation at reduced pressure

A—Optimum piston, one-fourth-inch cylinder
B—Optimum piston, $^5/_{16}$-inch cylinder

Circuit Equations

Mathematically the circuit of Figure 11 has a solution which parallels that of the series *RLC* circuit but is naturally more complex. The problem is treated as one of free oscillation in which an initial charge, Q_0, is placed on capacitor C. On account of the parallel paths in the circuit, three simultaneous equations are needed to express the fundamental relationships of current and voltage.

$$i_1R+L\frac{di}{dt}+\frac{q}{C}=0 \tag{1}$$

$$i_1R-\frac{q_2}{C_1}=0 \tag{2}$$

$$i-i_1-i_2=0 \tag{3}$$

From these equations it is required to find q as an explicit function of t, or at least to obtain enough information about the function so that the effectiveness of damping in the system can be judged. The steps in this procedure are given in Appendix I. It is shown that q is an exponential function of time with three terms

$$q=Q_1e^{\alpha_1t}+Q_2e^{\alpha_2t}+Q_3e^{\alpha_3t} \tag{10}$$

In this equation α_1 is a negative real quantity, which means that the first term represents an exponential decay without oscillation. The solution of α_2 and α_3 shows that these exponents are conjugate complex roots, a result which depends entirely on the algebraic solution and not on numerical values of circuit constants. Therefore, these terms together represent a damped oscillatory discharge with decrement and frequency given by the real and imaginary parts of the roots. For reference these will be called K and ω respectively. Taking the equation as a whole, the interpretation is a damped oscillation about an axis which is itself a curve of exponential decay. This appears plainly in Figure 4, curve C, which is reproduced from an oscillogram.

It is not necessary to complete the solution of equation 10 by evaluating Q_1, Q_2, and Q_3. The performance of the circuit can be understood sufficiently by examining α_1, K, and ω. An interpretation of these results is best obtained by comparison with the performance of the simpler series *RLC* circuit. This is generally familiar and is known to take the form of a damped discharge which may oscillate or not depending on the value of R. This points to two ways in which the circuit of Figure 11 differs from the series circuit. First, there is *always* oscillation, no matter what the value of R. Second, the oscillation occurs, not about the base line, but about an axis initially displaced, but which returns toward the base line as the discharge progresses. Thus there are two meanings of damping in such a system. One is the decrement in the oscillatory motion. The other is the rate at which the axis of the oscillatory motion returns to the base line. These two damping constants are both functions of R, but their behavior is basically different. It will appear from results to follow that one, the decrement of oscillation, is a function which starts at zero when R is zero, rises to a maximum, and declines again. The other damping constant is infinite when R is zero and declines continuously as R increases. Practically, the first seems to be of controlling interest. Calculations show that if it is at or near its maximum value the other is always much larger. This means that oscillation tends to persist even after the displacement in the axis of this oscillation has become negligible. Consequently the rate of decay of oscillation is the damping of primary importance.

The above analysis focuses attention on two quantities, K and ω. The first is a measure of the damping in the system. Reverting momentarily to the physical pendulum, the value of K does not by itself determine whether oscillation will be sustained under certain conditions of internally generated forces. It does however answer quantitatively two vital questions. In electrical terms these are (1) What value must R have to produce maximum damping with a given value of C_1? (2) If, on actual trial, this maximum is insufficient or requires too close tolerances on R, how may C_1 be altered to increase the damping? The quantity ω is not directly concerned with damping, but it determines the natural frequency of the system. This is of particular interest when the two axes of oscillation of the pendulum are considered together.

Since K as a function of R has a maximum and since this maximum is the most desirable operating point, it may be proposed that the required value of R can be found by differentiating and solving for R with the derivative set equal to zero. On account of the extreme complexity of the expression and the high powers of R encountered, this has not been done and is not thought to be possible algebraically. However, any specific case can be solved numerically, and this is the method used to study the performance of the damping cylinder.

Numerical Solution

Where numerical results are wanted, objection may be raised to the analogy method of solution. No fixed numerical equivalence exists between units of, say, moment of inertia and inductance. In the final result, however, this fact makes no difference. An arbitrary equivalence is assumed for one pair of analogous quantities and the relation of all others is thereby established. In the results worked out below, the electrical circuit represents the pendulum in its oscillation about the pendulum pivots.

Certain physical data about this system are required. Experimentally, the easiest to obtain are (1) The natural frequency of oscillation and (2) The rotational compliance. The latter can be found by measuring the force necessary to deflect the pendulum through a given displacement. From these tests the following values were obtained:

Natural frequency, $f_0 = 2.70$ cycles per second
$C_r = 9.72 \times 10^{-6}$ radians per dyne centimeter

Note that the results are constants of the pendulum and are obtained with the damping cylinder disconnected.

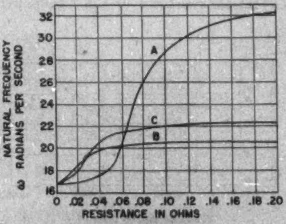

Figure 6. Oscillation frequency versus resistance (damping constant)

A—One-fourth-inch cylinder, 76 centimeters mercury pressure

B—One-fourth-inch cylinder, 14 centimeters mercury pressure

C—$^5/_{16}$-inch cylinder, 14 centimeters mercury pressure

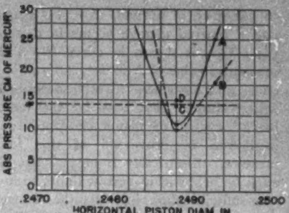

Figure 7. Pressure to stop oscillations versus horizontal-piston diameter

Cylinder diameter—Horizontal	0.2500 inch
Vertical	0.2500 inch
A—Vertical piston diameter,	0.2467 inch
B—Vertical piston diameter,	0.2476 inch
C—Vertical piston diameter,	0.2488 inch
D—Vertical piston diameter,	0.2496 inch

—pressure at 40,000 feet altitude

Returning now to the electrical circuit of Figure 11, values are to be assigned to C and L. Since the first choice is entirely arbitrary, it was thought convenient to let C be one farad. The value of L is then determined by the natural frequency and comes out 0.00347 henry. The next step is to evaluate C_1, which represents the forces due to compressibility of air in the damping cylinder. The conversion factor from rotational compliance to electrical capacitance is established by the choice of C already made, but it is first necessary to determine the value of the mechanical constant. This can be done entirely by calculation, knowing the dimensions of the cylinder, the length of the lever arm by which it is attached to the pendulum, and the behavior of air in compression. To illustrate the method, the calculation is given in Appendix II. To represent the cylinder at normal atmospheric pressure it is determined that the value for C_1 is 0.356 farad. Note that this calculation would have to be made whether the problem is solved electrically or mechanically.

From here on, the problem is entirely one of arithmetic. Circuit constants other than R have been assigned fixed values. The quantities K and ω are to be computed as functions of R so that curves may be plotted. Having done so for the case just assumed, it will be required to repeat for the condition of one-sixth atmospheric pressure. This sets a new value for C_1.

Numerical Results

Results calculated in this way are shown in Figure 12. Curve *A* represents the first condition, corresponding to normal atmospheric pressure. The maximum value of K is 11.9 corresponding to $R = 0.070$. When the pressure is reduced to one-sixth of normal, $C_1=2.14$ farads and a marked change occurs in damping. This is shown in curve *B*. The maximum value of *K* is only 2.0 and occurs at $R=0.024$. Tests described earlier showed that only by the most precise control of piston clearance can oscillation be prevented at this low pressure. Electrically it means that *R* must be held within narrow limits to insure sufficient damping. By the change subsequently made, increasing the cylinder diameter to $^5/_{16}$ inch, the corresponding electrical capacitance at low pressure becomes 1.37 farads and the *K* curve corresponding is *C*. The gain in maximum value of *K* is in proportion to the decrease in C_1. It shows how a safe margin was put on the damping available and a reasonable tolerance on the piston clearance.

The frequency at which oscillation tends to occur is not of such vital consequence as the damping, but it is of interest to note how it changes with *R*. Figure 6 shows curves of ω corresponding to *K* curves of Figure 12. In the beginning of the analysis of the circuit it was deduced that in the extreme conditions $R=0$ and $R=\infty$ the natural frequencies would be different. The manner in which the transition takes place is shown by the curves.

Experiments With the Electric Circuit

When results of the solution by electrical analogy are shown in the form of the quantities *K* and ω, no ready picture is obtained of the actual oscillations. To calculate these curves in full is too cumbersome mathematically. In order to fill this gap and at the same time check conclusions as to optimum damping resistance, an experimental setup of the circuit of Figure 11 was made. Since circuit constants originally assumed are not feasible electrically they were rearranged for convenience and chosen to give a frequency of about 300 cycles per second. The performance characteristics of the original circuit were preserved in the miniature version. In this way the voltage on capacitor *C* could be used to operate an oscillograph and record the transient. Since the oscillograph was of the magnetic type, the low resistance of the element circuit would normally act as a shunt on the capacitor. To overcome this, an amplifier circuit employing one tube was used. This offered substantially infinite impedance to the capacitor and so did not disturb the transient being measured.

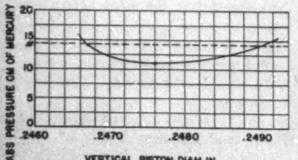

Figure 8. Pressure to stop oscillations versus vertical-piston diameter

Cylinder diameter—Horizontal	0.2500 inch
Vertical	0.2500 inch
Horizontal piston diameter,	0.2488 inch

‡—Pressure at 40,000 feet altitude

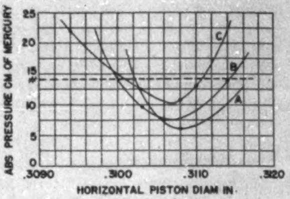

Figure 9. Pressure to stop oscillations versus horizontal-piston diameter

Cylinder diameter—Horizontal	0.3124 inch
Vertical	0.2500 inch
A—Vertical piston diameter,	0.2468 inch
B—Vertical piston diameter,	0.2470 inch
C—Vertical piston diameter,	0.2480 inch

‡—Pressure at 40,000 feet altitude

In Figures 3, 4, and 5 are shown a number of curves traced from these oscillograms. Interpretation of these has already been given in terms of the mechanical performance of damping cylinders. They show a number of limiting and ideal cases and others representing actual operating conditions. It is to be noted that a completely undamped wave cannot be produced on account of unavoidable resistance in the inductance. Neither could undamped oscillation be obtained in the mechanical case.

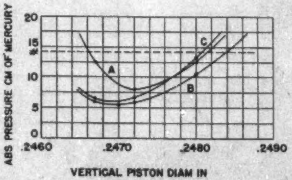

Figure 10. Pressure to stop oscillations versus vertical-piston diameter

Cylinder diameter—Horizontal	0.3124 inch
Vertical	0.2500 inch
A—Horizontal piston diameter,	0.3103 inch
B—Horizontal piston diameter,	0.3108 inch
C—Horizontal piston diameter,	0.3110 inch

‡—Pressure at 40,000 feet altitude

From the results of these electrical studies, both analytical and experimental, the optimum value of resistance for all conditions of operation is indicated. The electrical analogy has served its purpose and results obtained may be translated back into mechanical language. In particular, the proper damping constant for the cylinders is wanted. The information obtained in this way does not lead directly to the clearance between cylinder and piston diameter which must be used. This, however, can be established readily once the correct damping constant is known. Test results were included earlier in this article showing the oscillation performance of cylinder assemblies having the correct clearance and the effect of deviating either way from this value. In all, three approaches are used to the problem of optimum damping: electrical calculation; electrical experiment; mechanical experiment. By whatever method the results are reached, they are seen to be the same.

Conclusions

Air-damping cylinders have been applied successfully to the erecting system of a gyro-horizon instrument for aircraft. In so doing, a degree of damping is obtained which would not be possible by other means within the same limitations of space and weight. It is shown that these cylinders do not act as pure damping agents, but also introduce an elastic component of force, which varies in a relative magnitude with the amount of compression of the confined air and its absolute pressure. By a method employing an electrical analogy, the factors which control the ultimate damping effect are analyzed. The principles derived in this study are applied to assure adequate damping for all conditions of operation.

Appendix I

Solution of Circuit Equations

Equations 1, 2, and 3 may be rewritten with currents replaced in all cases by the time derivative of charge.

$$\frac{dq_1}{dt}R+L\frac{d^2q}{dt^2}+\frac{q}{C}=0 \tag{4}$$

$$\frac{dq_1}{dt}R-\frac{q_2}{C_1}=0 \tag{5}$$

$$\frac{dq}{dt}-\frac{dq_1}{dt}-\frac{dq_2}{dt}=0 \tag{6}$$

By algebraic substitution this may be reduced to a single equation in q alone, the variable which corresponds to pendulum displacement.

$$\frac{d^3q}{dt^3}+\frac{1}{C_1R}\frac{d^2q}{dt^2}+\frac{1}{L}\left(\frac{1}{C_1}+\frac{1}{C}\right)\frac{dq}{dt}+\frac{1}{CC_1RL}q=0 \tag{7}$$

For convenience in handling let this be expressed as

$$\frac{d^3q}{dt^3}+A_1\frac{d^2q}{dt^2}+A_2\frac{dq}{dt}+A_3q=0 \tag{8}$$

This is a linear homogeneous differential equation of the third order. As such it is known to have an exponential solution which may be written in the form

$$q=Qe^{\alpha t} \tag{9}$$

Since the complete solution of a third order equation must allow for three roots, it is known to have three parts. In its complete form, then

$$q=Q_1e^{\alpha_1 t}+Q_2e^{\alpha_2 t}+Q_3e^{\alpha_3 t} \tag{10}$$

Equation 9 may be differentiated to obtain the derivatives required in equation 8. Substituting, this gives the auxiliary equation

$$\alpha^3+A_1\alpha^2+A_2\alpha+A_3=0 \tag{11}$$

The three values of α to fit equation 10 will be obtained by solving equation 11, which, being a cubic, must have three roots. A convenient form of the solution is taken from Eshbach's "Handbook of Engineering Fundamentals," pages 2–13. Making changes in notation to agree with equation 8, let

$$p=\frac{A_2}{3}-\left(\frac{A_1}{3}\right)^2 \tag{12}$$

and

$$r=\frac{1}{2}\left(\frac{A_1A_2}{3}-A_3\right)-\left(\frac{A_1}{3}\right)^3 \tag{13}$$

Then define

$$S_1=(r+\sqrt{p^3+r^2})^{1/3} \tag{14}$$

and

$$S_2=(r-\sqrt{p^3+r^2})^{1/3} \tag{15}$$

The roots of α are now given by

$$\alpha_1=(S_1+S_2)-\frac{A_1}{3} \tag{16}$$

$$\alpha_2=-\frac{1}{2}(S_1+S_2)-\frac{A_1}{3}+j\frac{\sqrt{3}}{2}(S_1-S_2) \tag{17}$$

$$\alpha_3=-\frac{1}{2}(S_1+S_2)-\frac{A_1}{3}-j\frac{\sqrt{3}}{2}(S_1-S_2) \tag{18}$$

As noted in the text this is as far as the solution needs to be carried. The tendency of the system to oscillate and the frequency at which it oscillates are defined by the real and imaginary components of the roots α_2 and α_3. Letting these be K and ω respectively they are given by

$$K=-\frac{1}{2}(S_1+S_2)-\frac{A_1}{3} \tag{19}$$

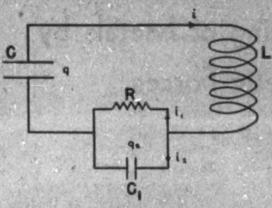

Figure 11. Circuit diagram, electrical analogy

$$\omega=\frac{\sqrt{3}}{2}(S_1-S_2) \tag{20}$$

Through these equations K and ω are seen to depend on the circuit constants L, C, C_1, and R, although for convenience they are expressed in terms of auxiliary variables. Results are given in the form of K and ω versus R for various assumed values of C_1 (Figures 12 and 6).

Appendix II

Calculation of Capacitance Corresponding to Damping Cylinder

It is required to determine how the force due to compression of gas in the damping cylinder may be represented in terms of "rotational compliance" in the pendulum system. First, consider linear forces in the cylinder. Let the piston area be A and the atmospheric pressure p_0. Denote the position of the piston by x and let its starting position be x_0.

If the piston is moved under conditions which do not permit the passage of air to or from the inside, then the pressure changes and a force, F, is exerted on the piston. It is first necessary to find F as a function of x, and take the slope dF/dx, which is the force per unit deflection. Letting p be the new pressure, the force is given by

$$F=(p-p_0)A \tag{21}$$

and p is related to x through the general gas equation

$$pv^k=\text{constant} \tag{22}$$

The exponent, k, is 1.0 for isothermal compression and 1.4 for adiabatic compression. The latter, involving no gain or loss of heat by the gas during the process, will be taken to represent the condition in the cylinder. Using equation 22 and replacing v by its value, xA, gives

$$p(xA)^k=p_0(x_0A)^k \quad \text{or} \quad p=p_0\left(\frac{x_0}{x}\right)^k \tag{23}$$

Substituting in equation 21

$$F=p_0A\left[\left(\frac{x_0}{x}\right)^k-1\right] \tag{24}$$

Then differentiating

$$\frac{dF}{dx}=-kp_0A\ \frac{x_0^k}{x^{k+1}} \tag{25}$$

To make future calculations workable it is necessary to assume that force per unit deflection is constant over the operating range of the cylinder. To make this approximation take dF/dx at $x=x_0$ and let x_0 be the midpoint of piston travel, or normal rest position. Making this change in equation 25 and at the same time substituting for k, its value 1.4

$$\frac{dF}{dx}=-1.4\frac{p_0A}{x_0} \tag{26}$$

Equation 26 expresses the spring constant of the cylinder for linear motion. If it is multiplied by the square of the radius arm, l, at which the force acts on the pendulum the result is the rotational spring constant and its reciprocal is the rotational compliance due to the damping cylinder.

$$C_r=-\frac{x_0}{1.4p_0Al^2} \tag{27}$$

To take a specific case use the one-fourth-inch-diameter cylinder for the pendulum, taking pressure as one atmosphere. Piston diameter is 0.2485 inch, and x_0, half of total travel, is $^3/_{16}$ inch. Lever arm, l, is 0.556 centimeter. Tabulating all quantities in the required units

$p_0=1.012\times10^6$ dynes per square centimeter
$A=0.314$ square centimeter
$x_0=0.476$ centimeter
$l^2=0.309$ square centimeter

from which $C_r=3.46\times10^{-6}$ radians per dyne centimeter. Converting this now to capacitance, using the equivalence already established

$C_1=0.356$ farad

This is the value of C_1 used in the text. It is subsequently recalculated for a pressure of one-sixth normal and again for a cylinder diameter of $^5/_{16}$ inch.

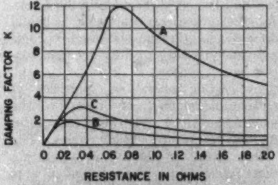

Figure 12. Effective damping versus resistance (damping constant)

A—One-fourth-inch cylinder, 76 centimeters mercury pressure
B—One-fourth-inch cylinder, 14 centimeters mercury pressure
C—$^5/_{16}$-inch cylinder, 14 centimeters mercury pressure

Reference

1. DYNAMICAL ANALOGIES (a book), H. F. Olson. D. Van Nostrand, 1943.

Treatment and Casting of Metals by Electromagnetic Forces

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THIS paper discusses certain methods developed some years ago employing electromagnetic forces for treating and casting metals.[1,2] The method of treatment is considerably more effective than any other internal-force action so far used for impurity elimination in metals. The electromagnetic forces used in the molten metal produce an effect equivalent to increasing its density many times without change in the density of the impurities. Other effects produced in the metal are an effective overcoming of viscosity, even in those metals noted for a high degree of viscosity, and a temperature gradient enabling progressive solidification in the direction of travel of the impurities to the surface.

The method of casting makes possible the production of structures of greater complexity than can be made with other methods, depending on either internal- or external-force action for casting pressure. Moreover, it achieves this result with considerable reduction in casting pressure for a given complexity of casting. It is probable that the use of this method in practice will reduce rejections due to flaws to a negligible proportion, even in those cases which are notorious for large rejections.

It is thought that these methods may be better appreciated and understood after a preliminary review of the normal factors in molten metal which are favorable and unfavorable for impurity elimination and perfect casting production.

Normal Forces on Discrete Particles of Matter in Molten Metal

Such common impurities in metal as gas pockets and oxide particles are lighter than the metal bodies which enclose them. Accordingly, the weight of these impurities in molten metal is exceeded by the upward force of buoyancy; in the absence of conditions leading to occlusion in the molten metal, the impurities rise and eventually reach its upper surface, either floating there if they are solid or escaping to the atmosphere if in the form of gas. As soon, however, as the impurities are set in motion in the metal, they are subjected to a frictional force produced by the viscosity of the metal and impeding their upward travel to the surface. These opposing forces of buoyancy and viscosity largely govern the extent of impurity elimination from molten metals.

All molten metals are capable of freeing themselves of undissolved matter in the foregoing manner, but the separating action becomes less and less effective with decreasing size of the impurities. Like the weight of the impurities, the upward force of buoyancy is a function of particle volume and thus remains constant on each unit volume of the impurities, regardless of their size. On the other hand, the opposing force of viscosity is a function of particle surface and thus increases on each unit volume of the impurities as they diminish in size. Consequently, the net force available to maintain upward travel of the impurities becomes less and less with their diminishing size. As the impurities approach colloidal dimensions, other physical factors come into play tending to keep them in suspension.

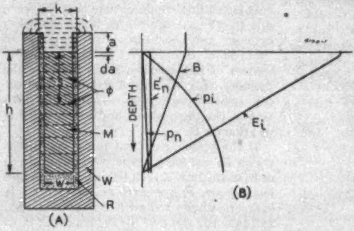

Figure 1. Increased buoyancy with converted field of current

A. Section taken at right angles to current path; *a* is the protruding distance of magnetic walls above metal, *k* the distance between vertical walls, and *h* and *w* the height and width of metal

B. Variations of flux density *B*, natural pressure p_n, induced pressure p_i, natural buoyant effort E_n, and induced buoyant effort E_i with varying depth

In casting operations, particles of undesirable matter may pass into the molten metal or otherwise take form therein, as a result of many causes. The molten metal is effective in separating these formed impurities only as long as it remains sufficiently fluid in the mold. One objectionable factor, in particular, in casting operations is the tendency of the cast metal to cool more rapidly in the upper layers. Once solidification takes place in these layers, all possible means of escape of interior impurities from the still molten portions of the metal are precluded.

In conclusion, insufficient buoyancy and fluidity of molten metal together with premature solidification of the upper layers constitute the fundamental reasons for particle occlusions in metals.

Method of Treatment by Electromagnetic Forces

Aside from force of gravity, there is only one other force which is now being used to any great extent for removing impurities from molten metal, namely, centrifugal force. This force not only requires the use of intricate apparatus for rotation of the metal, but deprives the metal of whatever benefit is derived from force of gravity. Its applications for impurity elimination are restricted largely to the production of annular ingots.

The proposed method of treatment employs electromagnetic forces which are unidirectionally obtained by the action of field on current and which add to the forces of gravity in the metal.

INCREASED BUOYANCY WITH CONVERTED FIELD OF CURRENT

Ordinarily, when current is passed through a conductor, the magnetic field which it establishes in the conductor is a nonunidirectional field which reacts with the current to produce nonunidirectional forces in the conductor. The interiorly directed converging forces normally causing "pinch effect" owe their production to this cause. It is possible by subjecting the field of the current to the action of iron masses appropriately disposed in the vicinity of the metal to convert it into a unidirectional field through the metal with an accompanying conversion of its forces into unidirectional forces. These forces can be produced in sufficient strength under these conditions to be used as the only electromagnetic treating forces in the metal.

One way of carrying out the aforementioned principle is illustrated in Figure 1A. The metal *M* is bounded at its sides and bottom by iron walls *W* of such permeability and thickness as to offer a negligible reluctance to the increased lines of flux ϕ established by the current. The lines ϕ travel horizontally between

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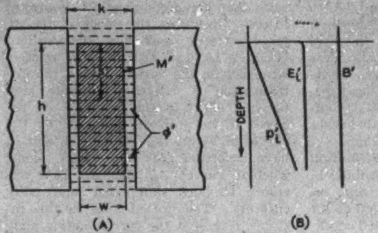

Figure 2. Increased buoyancy with separate field

A. Section taken at right angles to current path; k is the width of air gap, and h and w the height and width of metal

B. Variations of flux density B', induced pressure p_i', and induced buoyant effort E_i' with varying depth

the vertical walls because of their refraction by these walls and their tendency to follow a path of least reluctance. It is apparent that the flux density will be zero at the bottom of the metal and will increase to a maximum at its upper surface. An inner refractory lining R is shown containing the metal.

In accordance with the left-handed rule for motion of a current-carrying conductor in a magnetic field, a downward force is internally induced in the metal M, regardless of the direction of current flow. In the same manner as force of gravity, this electromagnetic force may be considered as made up of an infinite number of parallel forces acting on the mass elements. These, however, are not equal forces, but increase from zero at the bottom to a maximum at the top.

Assume that the metal M is a straight conductor and that a direct current I is caused to flow. Since the current density is uniform throughout the cross section of the metal, the flux density B (Figure 1B) assumes a straight-line characteristic lengthwise of the conductor height. As shown in Appendix A, the induced pressures at the bottom and at any given depth s in the metal have the values:

$$P_i = 2\pi I^2/kw \qquad (1)$$

and

$$p_i = [2\pi s(2h-s)I^2]/kwh^2 \qquad (2)$$

respectively, where k, h, and w are distances defined in Figure 1A.

The induced pressure at any point of the metal is transmitted in all directions. Inspection of equation 2 shows it to be zero when $s=0$ and a maximum $2\pi I^2/kw$ when $s=h$ (checking equation 1). The buoyancy of the metal is obviously increased.

Most common impurities in metal are nonconductive or comparatively nonconductive and will therefore be acted upon effectively by this treatment. Gas bubbles will be forced to rise in the metal and their upward travel to the surface accelerated. Slag or formed oxides also will be separated and forced to remain at the upper surface of the metal. The increased buoyancy will act on all impurities having less conductivity than the metal, whether of smaller or greater density than the metal.

Equation 2 also discloses the fact that the induced pressure is not proportional to the depth, but increases at a diminishing rate from the top of the metal to the bottom. This is clearly shown in Figure 1B by the curve for p_i plotted against depth. The induced buoyancy depends on the slope of this curve and must therefore increase with decreasing depth in the metal. This is a desirable feature in view of the rapidly increasing resistant force of viscosity on the impurities to be separated with their increasing momentum through the metal.

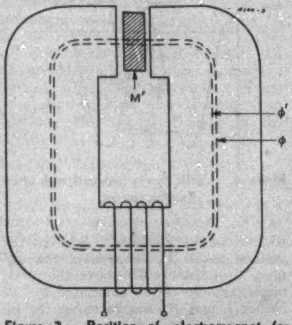

Figure 3. Position of electromagnet for proper treatment with separate field

The induced buoyant effort on an impurity of given size for any position it may assume in the liquid metal may be obtained from the following general relation:

$$E_i = Vf'(p_i) \qquad (3)$$

where V is the volume of metal displaced by the impurity, and $f'(p_i)$ is the derivative of the function $f(p_i)$ with respect to depth in the metal. For the particular conditions assumed, the induced buoyant effort relation becomes

$$E_i = [4\pi VI^2(h-s)]/kwh^2 \qquad (4)$$

The plot of this expression in Figure 1B neglects the variation in V at the bottom of the metal. The induced buoyant effort is zero at the bottom and follows a straight-line characteristic up to the point near the surface where the impurity emerges from the liquid metal.

Inspection of equation 4 shows that the induced buoyant effort at any point is inversely proportional to the distance k between the vertical portions of the magnetic channel. It is consequently desirable to reduce k to the smallest practicable distance. While the distance of upward travel of impurities from within the metal is increased by increasing the height h, the maximum value which the induced buoyant effort assumes near the upper surface is uninfluenced by any isovolumetric change in the dimensions h and w.

In Figure 1B, natural-pressure and buoyant-effort curves p_n and E_n are included for the purpose of showing the extent to which the method of treatment may be made to increase the buoyancy of the metal.

INCREASED BUOYANCY WITH SEPARATE FIELD

Referring to Figure 2A, a straight metal conductor M' carrying direct current is positioned in the uniform field of an electromagnet also carrying direct current. The relative directions of current and field are assumed to be such as to cause downward forces to be produced in the conductor. It can be shown that these forces produce the following values of pressure and buoyant effort at any given depth in the metal:

$$p_i' = 4\pi sINI'/kwh \qquad (5)$$

and

$$E_i' = 4\pi VINI'/kwh \qquad (6)$$

respectively, where NI' are the abampere turns of the electromagnet. Referring to Figure 2B, p_i' and E_i' are seen to vary in the same manner as natural pressure and buoyant effort.

On this treatment is superimposed the effect of the field of the current passed through the metal. If this field is not

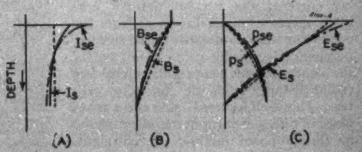

Figure 4. Effects of varying current with converted field of current

A. Effect on current density

B. Effect on flux density

C. Effects on induced pressure and buoyant effort

Subscript s has reference to skin effect alone, and subscript se to both skin effect and eddy currents

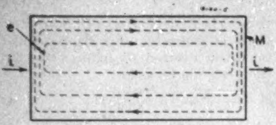

Figure 5. Metal *M* in side elevation showing path of eddy currents

made to produce treatment in the metal, it needs to be substantially neutralized by the superior strength of the separate field. Since the separate field cannot be increased beyond saturation of the electromagnet, there is a limit under these conditions to the value of current that can be passed through the metal. This in turn limits the magnitude of the forces that can be produced by the separate field.

It is consequently desirable that the field of the current passed through the metal be made to contribute its own treatment in the metal. In Figure 3 the electromagnet is so positioned as to convert it into a useful field ϕ assisting the field ϕ' in producing the downward forces desired. The resulting treatment in the metal is the sum of the treatments peculiar to the two fields.

Modified Effect of "Pinch" in Molten Metal

"Pinch effect" in a melted conductor carrying current is manifested by local recurrent cross-sectional contraction at various points of the conductor. The forces normally producing this contraction in the case of a straight conductor are converging forces tending to move the mass elements toward the center of the conductor. In the proposed method of treatment, the mass elements tend to move in one direction under the influence of the unidirectional forces employed in the molten metal. The local increase in current density resulting from an incipient contraction shows that the pinch effect is not eliminated, but the tendency of contraction may be reduced materially by these unidirectional forces.

Effects of Varying Current on Induced Buoyancy

It will now be shown that skin effect and eddy currents are additive effects similarly influencing the relations of induced pressure and buoyant effort with depth in each of the two cases of electromagnetic-force production shown in Figures 1A and 2A.

In a straight conductor carrying varying current, the current density normally increases in all directions from the conductor center outwards to the surface, because of decreasing magnetic linkages of the conductor filaments with increasing radial distance from the conductor center. This "self-induced" skin effect takes place in different form in metal subjected to the proposed method of treatment. Assume the metal *M* of Figure 1A to carry an alternating current. Because of the horizontal character of the field established through this metal, there are decreasing magnetic linkages of its filaments with increasing vertical distance from its bottom surface. Consequently, the current density increases from the bottom upwards to the top.

With this altered current distribution there occurs a changed flux distribution, the total flux inside the metal being reduced. Curves I_s and B_s in Figures 4A and 4B are examples of the altered conditions in the metal. The rms values of current density and flux density are represented by these curves, the dotted lines representing the corresponding distributions when a direct current of the same magnitude is utilized. Obviously, the effects illustrated by the curves I_s and B_s increase rapidly with frequency. They also increase with increasing conductivity of the metal, with increasing height *h* and width *w* of the metal, and with decreasing distance *k* between the vertical walls *W*. Increasing the distance *a* of these walls above the metal diminishes the effects illustrated by the curves I_s and B_s, but increases the inductance of the metal.

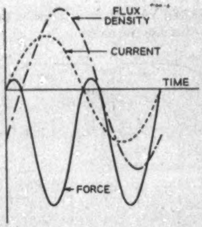

Figure 6. Cyclic forces induced with alternating current

If we refer to Figure 5, the eddy currents *e* flow in such a direction as to assist the load current at the top of the metal and to oppose it at the bottom. It is apparent that they increase in intensity toward the top and toward the bottom. The rms distribution of resultant current is shown by the curve I_{se} of Figure 4A whereas the rms distribution of resultant flux is shown by the curve B_{se} of Figure 4B.*

It is now possible to determine the distributions of effective pressure and buoyant effort in the metal *M*. Assuming a sinusoidal wave of current, the infinitesimal forces on the various layers have a wave form of the character illustrated in Figure 6. Though they reverse in direction at each passage of the current and flux-density waves through zero, they are predominantly downward forces. Observing the effect of the altered current and flux distributions (Figures 4A and 4B) on their relative strengths on the various layers, the distributions of effective pressure and buoyant effort in the metal *M* are believed to be properly indicated by the curves of Figure 4C. Curves p_s and E_s have reference to skin effect alone, whereas curves p_{se} and E_{se} have reference to both skin effect and eddy currents. The dotted lines in this instance represent the corresponding pressure and buoyant-effort distributions obtained with direct current of a magnitude causing equal pressure at the bottom.

The case of the metal *M'* of Figure 2A now will be considered briefly. When alternating current is passed through this metal, the use of an alternating field ϕ' of the same frequency as the current and of an appropriate phase relation therewith is needed to produce the downward forces desired. Under these conditions, the field ϕ' will force current toward the top in part because of a "mutually induced" skin effect and in part because of eddy currents, but the current-density variation set up in the metal by these phenomena will be influenced by the self-inductances of the metal filaments. Thus, in the absence of any straightening action on the field of the current passed through the metal, the curves p_{se}' and E_{se}' of Figure 7 are only approximate representations of the distributions of effective pressure and buoyant effort produced by the separate field. They truly represent these distributions only when the field of the current passed through the metal is made horizontal by the electromagnet core and therefore is caused to produce its own treatment in the metal, as shown in Figure 3.** Proper choice in any case should be made of the field ϕ', which should not be capable by its strength or otherwise of reversing the current in the bottom layers.

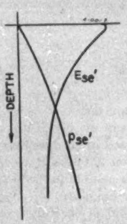

Figure 7. Effects of varying current on induced pressure (p_{se}') and buoyant effort (E_{se}') with separate field

To sum up, the use of varying current for electromagnetic-force production has the effect in all cases of forcing buoyancy toward the top of the metal under treatment.

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* See references 3 and 4 for a more detailed discussion of the current and flux distributions in iron-slot-embedded conductors carrying alternating current.

** In the case of varying current, the treatments of the two fields are not completely additive, but only approximately so.

INCREASED FLUIDITY EFFECT WITH VARYING CURRENT

The effects of the viscosity of liquids can be reduced by subjecting them to mechanical vibration. Similarly, when use is made of varying current for electromagnetic-force production in the molten metal, the effects of viscosity on impurity elimination are reduced by the agitation. Because of the unidirectional forces produced in the metal, the agitation is not of a turbulent type, but is a uniform agitation essentially characterized by the parallelism of its action throughout the treated mass. An effect similar to greatly increased fluidity is obtained.

The electromagnetic forces produced in a conductor carrying varying current are cyclic forces having twice the frequency of the current. Ordinarily, these forces are so directed in the conductor and so differ in phase and wave form at different points that a turbulent agitation is created which can hardly be regarded as being conducive to impurity elimination. If any vibrationlike movement is produced on the mass elements, it is superimposed on an intense stirring of these elements. Certainly, the impurities in their travel from the interior are impeded by their constant collisions with the mass elements. The effect of agitation normally produced by varying current is more likely to admix impurities with the mass elements than to favor their elimination from the melted conductor.

Referring to Figure 1A, the cyclic forces produced by passage of varying current through the metal M undergo their phase and wave-form variations in vertical planes. The motion of the mass elements is practically all in these planes, since the pressure in any given horizontal plane is everywhere substantially the same at any instant.* A constant up-and-down relative motion of all interior mass elements of the metal is produced, largely overcoming the resistant force of viscosity on impurities as a result of their vertical slippage relative to adjoining mass elements. The impurities are allowed to move under the action of buoyancy substantially unimpeded by the mass-element displacements, which are all parallel to their direction of travel to the upper surface.

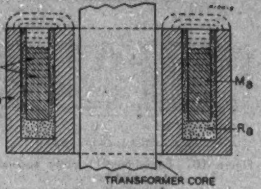

Figure 8. Sectional view of apparatus for production of annular ingots

In the case of the metal M' of Figure 2A, the use of varying current in producing the electromagnetic forces can be said to result in the same agitation. Strictly speaking, this is true only when the field of the current passed through the metal is made horizontal by the electromagnet core, as shown in Figure 3. In the absence of these conditions in the metal M', the effectiveness of the agitation is somewhat impaired by instantaneous-pressure variation in horizontal planes, which results in a certain tendency of motion of the mass elements in these planes. This is one more reason for treating the metal M' by the combined action of the two fields.

The agitation produced in molten metal under treatment by the method proposed is essentially an up-and-down vibratory motion of the mass elements which increases with increasing amplitude of the varying current. Since strong electromagnetic forces are desirable for increased buoyancy, it may be found advantageous to effect their production by the use of direct current with a superimposed wave of alternating current. In all probability a varying component producing slight vertical motion of the mass elements is all that is required to overcome the viscosity effects of the molten metal.

IMPURITY ELIMINATION DURING SOLIDIFICATION

Another effect of the treatment is a temperature gradient produced in the metal when using varying current. Since the current in the metal is forced by skin effect and eddy currents toward the top, the temperature in the metal accordingly increases from the bottom to the top. By proper choice of the factors on which skin effect and eddy currents depend, it is consequently possible to regulate the current through the metal on cooling to cause it to solidify from the bottom upwards. Impurities are eliminated effectively during solidification by this procedure because of progressive solidification in the direction of impurity elimination.

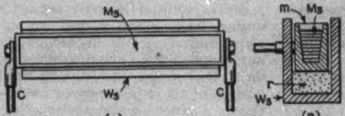

Figure 9. Apparatus viewed in top elevation A and in section B for production of rectilinear ingots

Of particular advantage is the temperature gradient for eliminating gases which the metal absorbs during the melting stage and which it ejects during solidification. Because of the temperature gradient in the metal, the gases progressively are thrown out of solution as the metal solidifies from the bottom to the top. The evolution of gases at any layer takes place just prior to solidification, so that the treatment has the effect of crowding the gases out as they are formed.

In the case of the metal M of Figure 1A, the temperature is practically constant at all points of equal depth. This is also true in the case of the metal M' of Figure 2A, but only approximately so in the absence of the conditions indicated in Figure 3. Once again it is apparent that these are the most desirable conditions for treating the metal M'.

PRACTICAL APPARATUS FOR TREATMENT

If we refer to Figure 8, the metal M_a under treatment forms a closed single-turn secondary winding of a transformer, the core of which is shown in part. Surrounding the bottom and sides of the metal M_a are the magnetic walls W_a formed as an annular channel. These should be of such laminated construction as to prevent them from acting as a secondary winding of the transformer. As shown, they are lined interiorly with a refractory R_a, which serves as a container for the metal M_a. The apparatus of Figure 8 is simply a low-frequency induction furnace altered to include the magnetic walls W_a.†

In low-frequency induction furnaces, the ring of metal normally is acted upon by outwardly directed radial forces of such strength that, in certain cases, the bottom of the trough can be seen. This phenomenon is due to the tendency of the metal to increase its magnetic linkages by embracing the largest possible area. It is entirely absent in the furnace of Figure 8, since a horizontal expansion of its iron-embedded ring of metal M_a produces no change of electromagnetic energy.

In each radial plane through the metal M_a, the current and the flux ϕ_a correspondingly produced distribute themselves in the same manner in vertical directions as previously discussed in connection with the use of varying current in the metal M of Figure 1A. In horizontal directions, however, both the current density and the flux density increase toward the inner-side surface of the metal M_a, which causes the various effects of the treatment to be more pronounced at this surface than at the outer-side surface. Also, the eddy currents in the metal M_a cancel one another in vertical directions and are thus equivalent to a flow of current in horizontal planes alone.

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* There is some slight variation of instantaneous pressure in horizontal planes resulting from the agitation itself, which causes constant ripples to be formed at the upper surface of the metal.

† Increased buoyancy was demonstrated strikingly with a small furnace of this type. The charge in its trough was melted and current thereafter interrupted. A small length of quartz tubing freely movable on a platinum wire was then introduced vertically in the molten metal. In this position the quartz tube under the action of natural buoyancy floated in the metal with a considerable portion extending below the surface. When circuit connections again were established, practically the entire length of the tube at once protruded above the metal.

The following relation developed in a manner similar to equation 1 is an approximation of the effective pressure induced at the bottom of the metal M_a:

$$P_a = 16\pi I_a^2/(d_4^2 - d_3^2) \log_e (d_2/d_1) \qquad (7)$$

where d_1 is the outer diameter of the inner vertical wall, d_2 the inner diameter of the outer vertical wall, d_3 the inner diameter of the metal, d_4 the outer diameter of the metal, and I_a the total effective current induced in the metal.

Since power factor is adversely affected by the magnetic walls W_a, it may be desirable to employ the furnace of Figure 8 in conjunction with a synchronous condenser or other line-power-factor booster. There may be various ways to increase the power factor of the furnace itself. One is to replace the annular walls W_a by an arcuate channel adapted for continuous rotation around the ring of metal throughout the period of treatment. In certain cases, such a channel could be used in fixed position, as when casting from a portion of the metal ring.

The proposed method of treatment also may be carried out in apparatus effecting a direct connection of the metal to a suitable current supply. If we refer to Figure 9, the metal M_s is treated in a metallic mold m removably positioned within magnetic walls W_s formed as a straight channel. A refractory block r supports the mold m, the ends of which are connected by conductors c to the current supply (not shown). For maximum efficiency the mold m should have its bottom and sides made of high-resistivity alloy and its ends made of comparatively low-resistivity metal.

The use of apparatus of the character illustrated in Figure 9 is recommended because of its adaptability to the use of direct current with a superimposed wave of alternating current. A homopolar machine may be used to supply a continuous current of large magnitude for increased buoyancy, on which current may suitably be superimposed an a-c component to overcome viscosity and produce a gradient of temperature to facilitate impurity elimination during solidification. The a-c component may be introduced through the secondary winding of a transformer in the circuit. The form of apparatus illustrated in Figure 9 is not at all restricted to the treatment of low-melting metals. Since treatment can be carried out in relatively short time, it should also be useful in connection with high-melting metals.

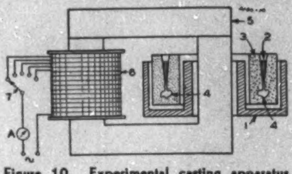

Figure 10. Experimental casting apparatus

CONCLUSION

This method of treatment by electromagnetic forces may be applied to all conducting metals and alloys for obtaining purer ingots. If carried out under the proper conditions, it should eliminate practically all nonconductive or comparatively nonconductive undissolved impurities from the treated metal. It may even be found effective in eliminating impurities so small that they would otherwise remain in colloidal suspension.

Method of Casting by Electromagnetic Forces

This method applies to those cases of casting requiring more casting pressure than can be obtained by gravity alone. The electromagnetic forces on which it depends eliminate all pressure piping and rotating parts necessitated by present methods. It is in this sense comparable to gravity casting, but reduces the amount of metal required to a minimum. It concentrates the casting pressure in the metal, in which the electromagnetic forces develop an electrodynamic head superimposed on a comparatively small hydrodynamic head produced by gravity.

EXPERIMENTAL WORK ON THE METHOD

Besides retaining the merits of gravity casting, the proposed method has important advantages of its own. This was recognized while experimenting with the method in the production of small castings of the type employed in dentistry. A general understanding of the method will be gained from these experiments now to be described.

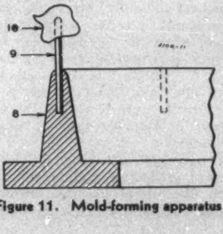

Figure 11. Mold-forming apparatus

If we refer to Figure 10, the experimental casting apparatus consisted of a small induction furnace provided with an annular receptacle 1 of solid iron for producing the casting forces in the single-turn secondary winding 2 of the metal to be cast. The receptacle 1 was radially slotted to prevent it from acting as a transformer secondary winding. The single-turn secondary winding 2 was a circular ring of about four inches in diameter, which was held in a narrow V-shaped trough formed in the upper portion of an annular mold 3 provided with casting ducts leading to its cavities 4. Insertion and removal of this mold were permitted by making the upper transformer-core part 5 separable from the remaining part. The primary winding 6 was fitted with a number of taps leading to a tap switch 7, which was used to compensate for current reduction with increasing temperature of the metal ring. A 110-volt lighting circuit was used as the a-c source.

The mold 3 was of the composition known in the dental trade as investment plaster. If we refer to Figure 11, the apparatus by which it was made included a trough-forming piece 8 provided with appropriate openings for the mounting of the usual duct-forming pins 9 supporting the wax patterns 10. The investment plaster was mixed with water to proper consistency, poured in the forming apparatus, and allowed to set. The main forming pieces then were removed, leaving the pins 9 protruding in the trough of the plaster mold. These were removed readily by heated pliers. Wax and surplus moisture were then expelled by baking in an ordinary domestic oven.

The mold upon its removal from the oven was immediately transferred to the receptacle 1, and the ring 2 melted as quickly as possible in its trough. Advantage was thus taken of the preheated condition of the mold to prevent congealing of the metal during its flow into the cavities 4. The casting into these cavities was coincident with the melting of metal above their respective ducts. After sufficient cooling of the mold to solidify the metal remaining in the trough, it was removed from the apparatus and placed in water for its disintegration. The castings were obtained with their sprues attached to the remaining ring of metal, from which they were freed for inspection.

In casting just at the melting point, the method was put to severe test. It was carried out successfully under these conditions with various low-melting-point metals, which included lead, tin, and even aluminum. Silver also was cast, but it was necessary to preheat the mold to much higher temperatures, which made the casting operation as a whole difficult to perform without breakage or undue cracking of the mold. Casting at the melting point was not, of course, intended to demonstrate its practicability, but was merely used for the purpose of experimentation.

When the iron receptacle 1 was omitted from the apparatus, it was found necessary to melt very slowly in order to avoid a complete and permanent break in the metal by pinch effect. On the other hand, when the iron receptacle 1 was included in the apparatus, the melting operation was carried out readily with marked increase of current density permitted by the downward forces. It was possible to melt at such speed as to cause a succession of breaks in the metal without permanent circuit interruption. These breaks were observed to occur at the bottom of the trough where fluid metal gathered after each break to reestablish a closed circuit.* The succession of breaks would last until there was sufficient molten metal at the bottom of the trough to permit melting without further circuit interruption. Such low-melting metals as tin were melted in this manner in less than two minutes.

Each of the various metals tried was rendered extremely sensitive to displacement into the mold cavities by the agitation created by the alternating current. Whereas the pressures used in present methods of casting in dentistry range as high as ten pounds per square inch, castings conforming in every detail with the original patterns were repeatedly obtained with a combined electro- and hydrodynamic head over the ducts of less than two pounds per square inch. This was all the more remarkable in view of the fact that casting was carried out just at the melting point. Moreover, it was found possible to cast through ducts of considerably reduced cross section. These were frequently of $^1/_{32}$-inch diameter as compared to the standard $^1/_{16}$-inch diameter of present methods.

As already mentioned, the expulsion of wax from the mold was carried out in an ordinary domestic oven. Present methods preheat the mold to much higher temperatures to drive off any surface residue of wax as a precaution against gas formation during the casting operation. The proposed method was not found to depend on preheating to such high temperatures for its satisfactory performance.

All these observations clearly pointed to fundamental difference from present methods. These are entirely dependent on pressure for conformity of the castings to the original patterns. The only path provided for the escape of air and other gas from the cavities is through the porous mold. If any gas is entrapped in the metal during its introduction into the cavities, it is likely to be retained permanently in the castings. The proposed method, in contrast, provides for increased fluidity of the metal filling the cavities, due to agitation transmitted from the upper body of molten metal. There is an additional path provided through this body of metal for the escape of gases from the cavities. A complete filling of the cavities occurs under these conditions with little chance of any gas being entrapped permanently in the castings.

The much lower casting pressures permitted by the proposed method reduce the danger of breakage of delicate portions of the mold. The dentist must now shape the inlay so as to avoid such fine edges or fins in the mold as would be destroyed by inrush of metal. The proposed method makes possible the production of castings of greater complexity than can be obtained by present methods.

PRACTICAL DENTAL-CASTING APPARATUS

The foregoing experiments in dentistry were carried out with exceptionally low casting pressure. In the form of apparatus illustrated in Figure 12, an extension 11 of the transformer core is used to direct transformer and leakage flux through a small portion of the metal ring. Increased power factor and transformer efficiency are obtained along with increased casting pressure.

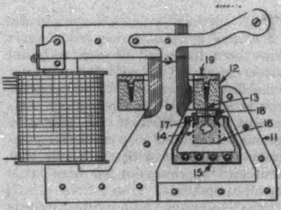

Figure 12. Practical dental-casting apparatus

The ring of metal is carried in a fixed crucible 12 of graphite or other refractory provided with a casting opening 13. Below this opening a single mold 14 is located carried in an electric wax evaporator 15. The mold has an outer perforated shell 16 for the escape of moisture, an upper ring 17 formed with a supporting flange snugly seated on the evaporator top, and a graphite disk 18 removably carried in the ring. The evaporator 15 may be carried on a track (not shown) adapted to lift the mold in sliding engagement with the bottom of the crucible. If desired, the crucible lid 19 may be fitted with a thermocouple (not shown) having its hot junction located in the crucible trough.

In operating the apparatus of Figure 12, the mold is suspended in the evaporator and heated therein for expulsion of wax and moisture. The evaporator then is manipulated to raise the mold against the crucible and seal the casting opening 13 with the graphite disk. The metal in the crucible now is melted and, upon reaching the desired temperature in the trough, the evaporator is moved to bring the mold in alignment with the casting opening 13. After sufficient time is allowed for the casting operation, the evaporator is moved to sever the metal in the mold from the metal in the crucible. The evaporator is allowed to remain in this position until solidification and thereafter is manipulated to withdraw the mold from beneath the crucible. The mold then is removed from the evaporator and thrown into water to recover the casting.

The apparatus of Figure 12 maintains the mold at the proper temperature during the casting operation. There are no sudden changes in temperature from the time the mold is inserted in the evaporator to the time it is removed to recover the casting. It is possible to control the rate of cooling of the casting by current regulation in the transformer and evaporator circuits.

The ring of metal may be formed conveniently by pouring molten metal in the crucible trough. This should place the ring under tensional stress on cooling and thus prevent crucible breakage by expansion on heating. Also, the snug fitting of metal in the crucible trough should tend to overcome permanent circuit interruption due to pinch effect. This difficulty otherwise may be corrected by so forming the crucible trough as slightly to reduce the cross section of metal where the casting forces are produced. The desired height of metal may be maintained by suitable additions of metal.

CONCLUSION

The proposed method may be advantageously applied to the casting of inlays or other dental forms, jewelry structures, and other fine forms in which accuracy of conformation and homogeneity of the structural mass are desirable. There are few if any perfectly sound castings of this type, entirely free from blowholes or other defects, produced by present methods. The proposed method materially improves the quality of casting obtainable by safeguarding against flaws of all types. A greater complexity of casting is possible with a minimum of casting pressure.

The advantages to be derived from the proposed method in dentistry should be of special importance in connection with the complex alloys of gold and platinum. The well-known tendency of the platinum to segregate has limited the use of these alloys to the factory production of crowns, inlay abutments, and other standard forms requiring no remelt by the dentist. The proposed method should make their successful casting by the dentist possible by preserving their homogeneity.

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* The occurrence of these breaks was accompanied by sparking creating a sizzling sound in the trough.

In view of the considerable reduction in casting pressure permitted by the method, it should be possible to apply it to all other cases of casting requiring greater pressure than can be obtained by gravity alone.

Appendix. Derivation of Equations for Induced Pressure at Bottom and at Any Point by Converted Field of Current

The metal M of Figure 1A is assumed to be a straight conductor carrying a direct current I. The symbols a, k, h, and w represent distances as defined in Figure 1A. The derivation is in centimeter–gram–second electromagnetic units.

If the reluctance of the iron path is neglected, the increase in magnetic energy, when the conductor moves a distance da, is

$$dU = [2\pi l I^2 da]/k$$

where l = the length of the conductor within the walls W.

Since the total force induced on the conductor is equal to dU/da, the induced pressure at the bottom is given by the equation:

$$P_1 = 2\pi I^2/kw \qquad (1)$$

To calculate the pressure at any depth, imagine the conductor divided into two distinct portions, the upper one having a height s. The energy increase, when the upper portion moves a distance da, is

$$dU_s = [2\pi l I_1^2 da]/k + [4\pi l I_1 I_2 da]/k$$

where I_1 and I_2 are the currents in the upper and lower portions, respectively. If we substitute their values in terms of the total current, dU_s becomes

$$dU_s = [2\pi l s^2 I^2 da]/kh^2 + [4\pi l s(h-s) I^2 da]/kh^3$$

Since the total force induced on the upper portion is equal to dU_s/da, the induced pressure at any depth is given by the equation:

$$p_1 = [2\pi s(2h-s)I^2]/kwh^3 \qquad (2)$$

Equation 2 can also be derived by considering the action of the magnetic flux on the current-carrying elements of the conductor.

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Design Considerations in Aircraft Instruments to Meet War Service

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Synopsis: New conditions, imposed by the rapid advancement in aviation, have challenged industry to develop instruments capable of meeting rigid war requirements. This paper describes by general treatment and by reference to examples, how the design features of aircraft instruments have been influenced by wide ranges of operating temperatures, extreme conditions of shock and vibration, extension of altitude ranges, worldwide climatic conditions, adoption of new power supplies for aircraft, standardization of mechanical features necessary because of large-scale use, and the requirements of servicing. The design features discussed are generally applicable to all aircraft instruments.

THE spectacular developments in the field of aviation during recent years have been the result of mass technical achievements. The realization of greater reliability, lighter weight, and smaller size at acceptable costs in any one component has challenged the inventive genius of engineers responsible for associated equipment with the result that continued improvement has been made on all components.

The importance of aircraft instruments is exemplified by the change in flying technique. Flying was accomplished with five or six instruments, aided by "a seat-of-the-pants" instinct. Today's blind flying relies entirely upon instruments for attitude and direction of the airplane. Multiengine airplanes and improved control devices have further increased the responsibility of instrumentation.

In some cases over 100 instruments are required on each airplane to indicate adequately the required functions. These instruments include those shown in Table I. The designs of these devices are not static. Their designs are under constant scrutiny, and every effort is made to improve them as required to meet new factors.

The rapid advancements in the field of aviation have influenced the design trend of aircraft instruments to meet more rigid requirements of war conditions. Improvements made necessary by war requirements will make possible further advances in aviation in the postwar period.

Noteworthy in the improvements in aircraft instruments is their ability to operate satisfactorily under the following conditions:

1. Wide range of ambient temperatures, −65 to +82 degrees centigrade.
2. From sea level up to 50,000 feet altitude.
3. Under severe test conditions of vibration up to 55 cycles per second at 0.020-inch amplitude.
4. Under abnormal shocks of 50 gravity acceleration.
5. Worldwide climatic conditions from +82 degrees centigrade at 100 per cent relative humidity to −65 degrees centigrade.

In addition to the wide range of operating conditions, it has been found essential to promote standardization of design insofar as possible from the standpoint of installation, reading, and servicing. Operating conditions also have changed as new types of airplanes and new flying techniques have been developed. Special features in aircraft instruments have met these requirements. Mechanical design has kept pace with the problems imposed by the use of larger power plants and new armament.

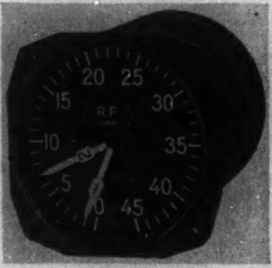

Figure 1. Magnetic-drag-type tachometer, dual element

Temperature Range

The extremes of ambient temperatures to which instruments are subjected will cause undesirable performance if design

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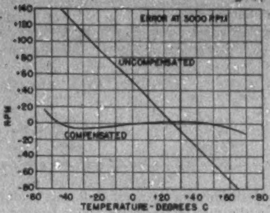


Figure 2. Compensation curve, magnetic-drag-type tachometer

consideration and compensation for these conditions are not provided. Effects noted with variations in temperature include:

(*a*). Variations in resistivity.

(*b*). Change in magnetic characteristics.

(*c*). Dimensional changes.

(*d*). Changes in physical properties and characteristics.

(*e*). Change in rate of chemical activity of materials.

The method used in the correction of calibration errors due to temperature changes is termed "temperature compensation." Extension of the ambient-temperature ranges has necessitated new techniques in temperature compensation as exemplified by a drag-disk magnetic tachometer, Figure 1. The uncompensated and compensated errors of this type of device may be seen in Figure 2. To achieve compensation, the control springs are manufactured to precise characteristics, and the drag-disk is made of a new material having a low temperature coefficient.[1] A nickel-alloy compensator is used to correct the temperature coefficient of the magnets.[2]

Mechanical design of the compensator is such that the optimum characteristics of the material are realized, and manufacturing or servicing is easy and simple (see Figure 3). Stabilization of the magnets to insure permanency includes exposing the magnets to temperature extremes beyond that expected in service.

Temperature compensation is also affected by other means as illustrated by a permanent-magnet-type ratio instrument, Figure 4. Reference to the schematic diagram, Figure 5, shows that by the proper proportioning of the shunt resistance, R_1, wound with low-temperature-coefficient wire to the series-coil resistance, R_2, correction can be made for changes in resistance of the main operating coil, R_3. This method is effective over the wide ranges of temperature necessary. Temperature compensation by means of bimetal parts is used extensively in mechanical systems. This technique is highly developed, and many ingenious designs are in use.

In some designs of electric-power load ammeters, higher millivolt drop than the normal 50 millivolts is used, allowing a greater percentage of low-temperature-coefficient resistance to be used in series with the copper-wound armature coil. This is significant, since such designs become almost independent of external lead resistance from the standpoint of accuracy and temperature error when lead resistance is kept in the order of one ohm or less. Negative-temperature-coefficient materials have been reported[3] as having characteristics which are desirable where series compensation can be used, and such materials are being investigated in an effort further to improve temperature compensations.

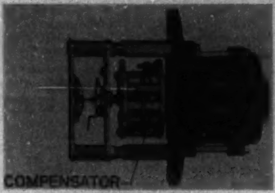


Figure 3. Magnetic-drag-type tachometer assembly showing temperature compensation

The temperature indicator, previously referred to, also illustrates the consideration which must be given to dimensional changes in materials due to temperature variations. By using materials having similar temperature coefficients of expansion, as shown in Figure 6, the close tolerances for end play between the pivots and the jewels are maintained.

Extension of the temperature ranges has created problems of lubrication, particularly in instruments having rotating members such as tachometers and gyroscopic devices. Relatively low torque scope with increased friction caused by high viscosities of lubricants at low temperatures. Low-viscosity lubricants are not satisfactory because of high evaporation rates at elevated temperatures, resulting in early loss of lubricant and short life of the instrument. Two approaches have been used toward solving this problem:

1. The field of natural lubricants, such as petroleum and animal products, has been examined carefully and those having the most desirable over-all characteristics selected. Tremendous strides also have been made in producing synthetic lubricants having unique lubricating properties. By the judicious use of these lubricants in the proper controlled quantities, a compromise between friction drag and life expectancy results in satisfactory instrument performance.

2. New instruments are designed to minimize temperature and lubrication problems. Electrically driven gyroscopic instruments, for example, Figure 7, have motors with high torque, in order to overcome low-temperature starting difficulties. Because of this, adequate amounts of lubricant are used to insure long bearing life. The effect of high temperatures is further minimized in comparison with previous designs, since the instruments have sealed cases and air is not drawn continuously through the bearings, which previously resulted in high oxidation and evaporation rates.[4]

0
50
100
°C
50
150

Figure 4. Ratio-type temperature indicator

Design of cylinder-head-temperature thermocouples has been revised radically. Thermocouples are now placed directly in the engine cylinder's walls, thus improving the reliability and accuracy of this important measurement.

Consideration is given to avoid the use of materials, such as certain plastics, which lose all dimensional stability at high temperatures and other materials which become glass-brittle at low temperatures. Since most chemical reactions increase rapidly at high temperatures, the selection of gaskets, varnishes, finishes, and other materials is carefully made to avoid premature failure. Combinations of materials must be such that undue chemical reaction will not occur.

Vibration

Vibration of aircraft has presented a most troublesome problem. Vibration causes wear of the mechanism, particularly the pivots and jewel bearings, and resonance effects eventually have led to failure of the mechanisms and associated structures.

Consideration has been given to changes in pivot materials. Hardness is a factor to consider, and its importance must be balanced with that of toughness. For light moving systems of 0.5 gram or less, high carbon steel with cone angle of 55 degrees and tip radius of 0.001 inch to 0.0015 inch nominal are satisfactory. For heavier systems, a tantalum–tungsten-alloy pivot is found best because of "toughness." These require a special hardening technique. The alloy-tipped pivots, made of natural alloys such as the platinum group, are now available commercially as corrosion-resistant bearings.[5]

With the increased vibration and shock requirements, the critical period of pointers has been analyzed and the designs revised where necessary to change resonance to a point beyond the range of test frequencies. Improved methods of connecting pointers to supports have been necessary. Material changes have been made from soft aluminum to stronger heat-treatable alloys.

A peculiar phenomenon which has caused frequent trouble in spring-controlled units has been the oscillation of the control springs when vibrated at or near their resonant frequency. The movement of these relatively heavy flexible mounted masses is very large. Precautions have been taken to prevent the spring turns before becoming entangled with adjacent parts. By reference to Figure 3, it can be seen how the balance arms on the shaft in one device are formed to prevent trouble. Spring design in general must be analyzed carefully to meet required conditions.

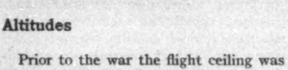
Increased vibration due to larger aircraft power plants has affected the mechanical design of direct-engine-mounted devices, as exemplified by the tachometer generator, Figures 8 and 9. Seal wiring of all parts capable of being moved by vibration is essential. End castings have been strengthened by the use of reinforcing webs, and single-piece castings have replaced several parts. Failure of the stator leads at the point of soldering to the receptacle pins was experienced until means were provided to clamp the leads to prevent vibration.

Altitudes

Prior to the war the flight ceiling was 30,000 feet. Instruments now must withstand the conditions existent at 50,000 feet. The effect of altitude also must be considered when problems associated with the rate of change are reviewed. Diving rates up to 700 miles per hour from these high altitudes subject the instruments to very rapid changes in pressure, temperature, and humidity conditions.

Air inside instrument cases is drawn in and forced out with changes in pressure. This cycle is known as "breathing." The combined effect of high altitudes and "breathing" does two things:

1. When the airplane is flying from hot humid conditions to high altitudes, the moisture in the instrument is condensed on the component parts.
2. When the airplane is returning from high altitudes to humid conditions at higher pressure, the air in the instrument case is filled with moisture.



R_1, shunt coil (copnic)
R_2, series coil (copper)
R_3, operating coil (copper)

Figure 5. Schematic diagram for a permanent-magnet-type ratio instrument

Table I. Aircraft Instruments

Flight	Navigational	Engine	Miscellaneous
Bank and climb	Automatic pilot	Tachometer	Position indicator
Altimeter	Directional gyroscope	Synchroscope	Ammeter
Bank and turn	Clock	Fuel gauge	Voltmeter
Rate of climb	Compass	Flow meter	Wattmeter
Air speed		Pressure	Frequency meter
		Temperature	
		Manifold pressure	

In order to minimize these deleterious conditions, instruments are provided with breathing means to permit quick equalization of pressures inside and outside the cases. The accepted way of accomplishing adequate breathing is by means of one or more holes drilled in the bottom of the case. This also permits accumulations of moisture to drain.

Protective shock mounts have been designed and applied to aircraft to isolate vibration disturbances from supporting structure which occur within mounted equipment and to protect delicate mechanisms, such as aircraft instruments, from disturbances.

Rubber shock mounts have been used unsparingly between engines and airplane structure in modern aircraft, while aircraft-instrument panels and instrument panels on associated radio and power units also have been shock-mounted. A discussion of vibration isolation in war machines was recently treated in detail.[5] Full consideration of reducing the amount of vibration and shock on instruments is important.

The latest Government specifications vary somewhat in their requirements, but in general aircraft instruments must withstand vibration of 0.018 to 0.020 inch amplitude at frequencies of 500 to 3,000 cycles per minute, usually for a period of two hours, without loosening of or obvious damage to mechanical parts, and a calibration change of not over two per cent of full-scale value in the case of ammeters and voltmeters. Such tests are made at various speeds to simulate aircraft vibration and to denote whether or not resonance of instrument components is within the operating band.

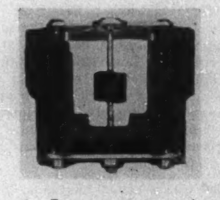


Figure 6. Temperature-indicator frame and shaft assembly

Frame is die-cast aluminum
Shaft is drawn-rod Dural

Specifications have been standardized[6] to cover a prescribed method of testing for shock at 50 gravity acceleration for ten times in three different airplanes. All aircraft instruments are being made subject to this new test, and changes in mechanical construction are being made where necessary in order to have them meet shock or abuse tests. It is anticipated that even sturdier instruments will be required to meet the rigid demands of larger aircraft power plants and cannon armament. Observations indicate that frequencies as high as 600 cycles per second of 50 gravity acceleration will also be experienced.

With the advent of the war, the supply of sapphire jewels from Europe was cut off, making it necessary to turn to a substitute of improved glass vee jewels as pivot bearings. These new jewels have been described[7] as having coefficients of friction comparable with sapphire and, when used with highly polished steel pivots, generally have produced less friction increase than sapphire bearings. The use of special improved glass has provided jewels which will withstand 60 per cent as much shock as sapphire. This is entirely satisfactory for instruments having weight of moving system of one gram or less, as has been proved by tests.

Greater shock- and vibration-resistance requirements also have affected the pivot design in some electric instruments. The relatively sharp-radius highly polished steel pivot, after being subjected to

Figure 7. Electrically driven gyro horizon indicator

Figure 8. Tachometer generator, before modifications

extremes of vibration and shock, will "hook" or bend, "mushroom" or flatten, and may wear to a much larger tip radius with time. Such change of pivot condition increases jewel roll or zero uncertainty; and if moving systems are adjusted to normal jewel play before abuse, the clearance is increased because of pivot contour change. This allows greater pivot movement in a jewel with subsequent vibration, until failure may occur.

Experience has shown that increased initial pivot radius is necessary. In a new line of small panel aircraft instruments, the pivot radii are made over 50 per cent larger than standard and 250 per cent larger for extreme applications. This solution is satisfactory and enables the standard high-carbon-steel alloys to be used.

Troubles due to excessive temperature rise, caused by increases in altitudes and rarified atmospheres, have not affected electric aircraft instruments[9] as has been the case with power generators and other similar devices. However, temperature rise caused by self-heat is not overlooked in instrument design.

Figure 10 shows the change in dielectric strength of air with altitude. Similar reduction in insulation strength occurs in all insulating materials at high altitudes. Large factors of safety are incorporated in the insulation of electric instruments to insure satisfactory operation.

The change in altitude requirements has affected those instruments operating from barometric pressures by increasing the pressure differentials to which they are exposed. Altimeters, for example, necessarily have had their operating ranges extended. Devices using air damping or air-jet reaction have been reviewed to insure that the low air densities have not influenced performance.[10] In some instruments the designs were made such that this problem was avoided.[11]

Figure 9. Tachometer generator, after modifications

Peculiar effects have been noted in connection with fuel-quantity gauges. Nonrigid self-sealing gasoline tanks, a war necessity, are subject to change in shape because of rapid variations in atmospheric pressure. The designs of the fuel-quantity tank units are such that these changes can be accommodated without damage to the unit.

Weather Conditions

The extended use of aircraft all over the world has made it essential to design components, such as instruments, so that they will withstand all types of desert, arctic, and tropical conditions. Reference has been made to the temperature ranges experienced.

Airplanes used in the deserts, are subjected to air currents containing minute particles of dirt, which cause instruments to become inoperative. Dust-tight seals for cover glass to case, case to base, connections to case, and other case joints, have been made to reduce trouble of this nature.

Inaccurate indication of direct-connected pressure devices, such as oil-pressure indicators, was attributable to congealed oil in the lines during cold-weather operation. Development of an improved system was completed. The difficulty described is avoided by dividing the pressure line by means of a diaphragm and using a low-freezing-point liquid for the pressure transmission.[12]

In conditions of high humidity experienced in tropical climates, better protection of component parts against corrosion and rust has become paramount. It is necessary to protect more effectively ferrous metals by the use of bonderizing, galvanizing, cadmium protected with tin, baking enamels, and other approved means in definite finish schedules.

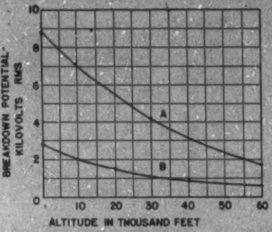


Figure 10. Breakdown potential versus altitude (air)

Hemisphere gap—one-fourth inch diameter tips
60-cycle supply—temperature 23 C
Relative humidity 33 per cent

A—$^1/_8$-inch spacing
B—$^1/_{32}$-inch spacing

Aluminum and magnesium alloys are better protected by the use of oxide coatings or other inert coatings as base coats prior to the application of standard finishes. Insulations of the organic nature have been improved, and solvents more adequately baked out, to the end that components are more weather resistant than ever before.

The growth of fungi is particularly troublesome, and its effect can be reduced greatly by the proper choice of organic materials or coatings, which, wherever possible, are fungus-resistant. Further work is being done on this to provide more fungusproof components.

A secondary problem in connection with the design of instruments is the suitable protection of the device to withstand "dead-storage" conditions, including complete submersion, prior to its actual use. Metal packaging may prove to be the solution.

Power Supplies

Direct current from batteries and generators has been the primary source of electric power on aircraft. Many instruments, particularly those of the remote-indicating type, are designed to use this power. Examples of such in-

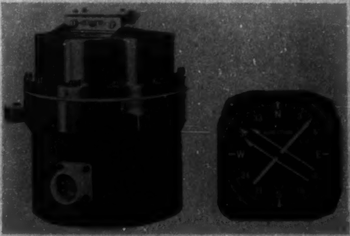


Figure 11. Remote-indicating compass, for 400-cycle operation

Transmitter on left
Receiver on right

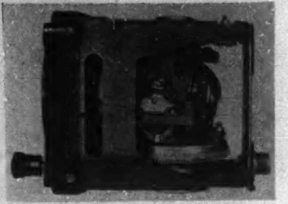


Figure 12. Electrically driven directional gyroscope, cover removed

Table II. Types of Dial Marking

Title	Description	Color
Luminous radioactive	Emits light without external excitation	Green
Phosphorescent luminescent	Requires ultraviolet excitation. Continues to emit light for a period after removal of excitation	Green or orange
Fluorescent radioactive	Emits light without excitation; is excited to greater brilliancy by external excitation	Yellow
Fluorescent	Requires ultraviolet excitation. Emits light only when excited	Yellow or red
Nonradiating	Emits light by reflection only	White, red, yellow, green, and black

struments are liquid-level indicators, landing-gear and flap-position indicators, and temperature-indicating devices. A prime requisite is that the effect of voltage variation be negligible. Instruments for nominal 24-volt systems are tested at 22 and 28.5 volts. Ratio-type instruments of both the moving-coil and permanent-magnet rotor designs have proved satisfactory.[13]

Much work has been done by the Armed Forces and airplane manufacturers to evaluate the relative merits of d-c and a-c systems for large airplanes. A recent study[14] indicates that an a-c system of 400 cycles may be the most efficient. Instruments similar to those already listed are duplicated in designs suitable for the 400 a-c power and are in widespread use, particularly on certain specific airplanes.

The most interesting developments are those instruments which exist only because a-c power has been made available. Instruments in this category are:

(*a*). Remote-indicating compasses.

(*b*). Gyroscopically stabilized azimuth indicators.

(*c*). Gyro flight indicators.

(*d*). All-electric automatic pilots.

New principles of operation[15] are used in the compasses to achieve results unobtainable by other means. The urgent need for remote compass indication in heavily armored fighter planes, where direct-reading compasses are of little value, is satisfied by this really significant contribution to the art of instrumentation. Typical examples of this type of 400-cycle a-c-operated remote-indicating compasses are shown in Figure 11.

Electrically operated gyroscopes have been referred to previously. Figure 12 shows the type of hysteresis motor used on a-c systems. Constant speed, simplicity, good starting torques, and freedom from troublesome commutation are characteristics directly attributable to a-c design.

By the incorporation of suitable controls and induction pick-offs in the gyroscopes, all-electric automatic pilots have been made available. Electricity has integrated the functions of indication, control, and power required for this application.

The adoption of 400-cycle frequency has created a need for electric indicating instruments comparable in function to those required for use on commercial frequencies. Measurements of voltage, current, watts, frequency, and other quantities will be made by indicating instruments under this new system. New designs must be made. Instruments available today do not satisfy the requirements of light weight, compactness, and case-mounting dimensions. The development of small 400-cycle wattmeters and frequency meters to be used with small-size ammeters and voltmeters which are now available will represent a definite advancement. Instruments of this type are in the development state, and samples are being tested in connection with the 400-cycle program.

Standardization of Mechanical Features

The expanded aircraft program required tremendous quantities of instruments. Many additional manufacturers were necessary to meet these needs. No real attempt at standardization of mechanical features had been made prior to the war, and such conditions obviously led to confusion. Through the co-operation of the War Production Board and the co-ordinated branches of the armed services, standardization has been accomplished.

Such standardization is of great assistance to the aircraft manufacturer by providing uniformity of space requirements and instrument panels. Figures 13, 14, and 15 show standardized cases.

The burden of the pilot is relieved by the use of instruments which are more uniform in appearance. Significant in this is the adoption of uniform style of letters and figures for use in dial marking. Dial arrangements have been modified to increase readability and reduce the possibility of reading errors. Typical of this is a dial arrangement as shown in Figure 16.

A major contribution to the standardization program is the use of Army–Navy-type connectors. This method of mak-

Table III

Instrument	Range	
	Old	New
Tachometer	0–3,500 rpm	0–4,500 rpm
Air-speed indicator	0–500 mph	0–700 mph
Altitude	0–40,000 feet	0–50,000 feet
Manifold pressure	0–50 inches	0–100 inches
Temperature range	−10 to +100 C	−70 to +150 C
Flow meters	0–300 gal per hr	0–600 gal per hr

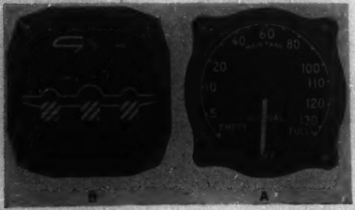


Figure 13 (left). Large-size instrument case

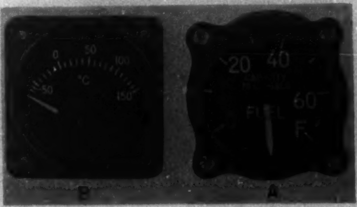


Figure 14 (right). Small-size instrument case

A. Old style
B. Army–Navy standard

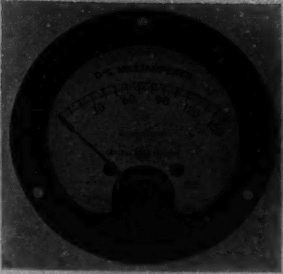


Figure 15. A 3$^{1}/_{2}$-inch American War Standard C39.2–1944 Instrument

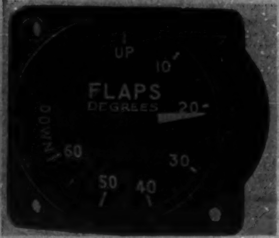


Figure 17. Remote-indicating flap-position indicator

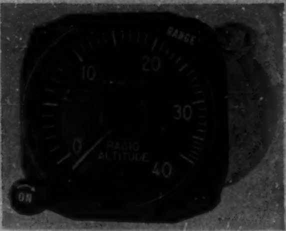


Figure 18. Radio altitude indicator, dual range

ing electric connections permits the instrument designer and user to work in perfect harmony, since interchangeability is assured.

Standardization has also been made on many miscellaneous items, such as cover glasses, mounting screws, stud sizes, and other instrument parts. This assists in installation, servicing, and stocking problems.

Miscellaneous Design Features

Lighting requirements for instruments have changed rapidly in view of new service conditions. Radioactive luminous materials and indirect white incandescent lighting have been supplemented by other forms. By the use of these materials, all dial-marking requirements are being met. Table II gives the dial-marking materials available and their description.

There has been a general improvement required in airplane performance. This has affected instrumentation by increasing the functional range of many types. Some of the changes are shown in Table III.

Ranges were also expanded to obtain greater accuracy in reading. Sensitive-type tachometers and barometric-type altimeters are examples. Remote-indicating flap indicators, as seen in Figure 17, show the expanded indications of angular position. Another example of range expansion is the radio-altimeter indicator, shown in Figure 18. A range-changing mechanism was adapted to the dial plate, so that, by turning a knob, the scale numerals could be changed in a ratio of 1 to 10, and at the same time, changes in the electric circuit could be made to agree. This mechanism is illustrated in Figure 19.

The increase in number of instruments required to indicate the functions of the airplanes has necessitated the placing of them in close proximity to each other. Such condition has resulted in magnetic interaction between two or more devices. Careful design attention to magnetic shielding has greatly minimized this effect. Figure 20 shows the application of magnetic shielding to a remote-indicating liquid-level transmitter for a special application.

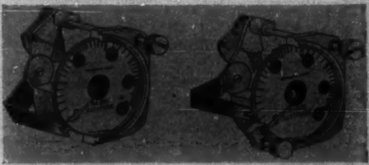


Figure 19. Diagrammatic sketch of radio altitude indicator, dual-range scale-changing mechanism

Servicing

For the large number of instruments required for aircraft and associated apparatus which are in use today in all parts of the world, one of the major requirements is that of servicing.

Standardization, interchangeability, and inherent design details contribute to the reduction of servicing and maintenance difficulties. The standardizing of units already has been discussed.

Interchangeability of component parts of units is exemplified by the tachometer indicator, Figure 1. The motor and drag-disk assemblies for the right- and left-hand elements are completely interchangeable. Aircraft panel-type ammeters, Figure 21, have a common millivolt rating for use with different ratings of external shunts. The entire instruments and parts can be interchanged for required current ratings by changing only the dial. Figure 22 shows these adjustments in the transmitter.

The entire electrical portion of the transmitter may be removed from the gauge without disturbing the portion attached to the gasoline tank or without removing the contents of the tank.

Temperature indicators of the permanent-magnet type are inherently simple in construction as compared to the moving-coil design. Figure 23 illustrates the unit armature construction and the

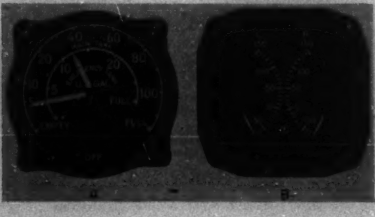


Figure 16. Comparison dual-indicator dials

A. Concentric-type pointers
B. Opposed-type pointers

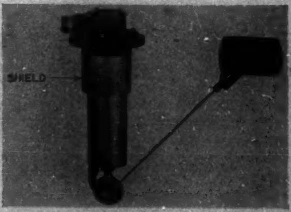


Figure 20. Remote-indicating liquid-level transmitter with magnetic shielding

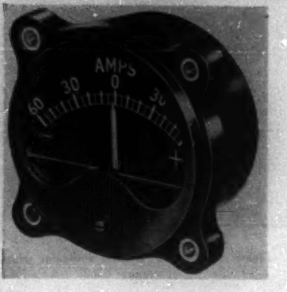


Figure 21. Aircraft panel-type ammeter

Figure 24. Typical aircraft instruments, front-mounted

sturdy construction of the element assembly.

The electrically driven gyroscopic instruments, Figure 12, can be inspected by removing the cover with the mechanism remaining intact. This aids in locating trouble and simplifies overhaul and adjustment.

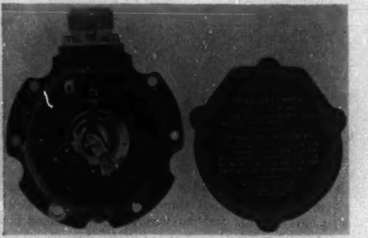


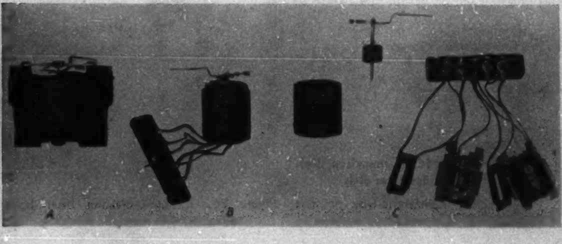
Figure 22. Remote-indicating gasoline-gauge transmitter, top view showing provisions for empty and full adjustment

The effort that has been put into the preparation of instruction books and maintenance manuals should not go unrecognized. Servicing and maintenance would be impossible without these aids.

Instrument-panel designs have been modified to enable the instruments to be front-of-panel-mounted. The ease of servicing is increased greatly, since any instrument can be replaced completely from the front of the panel. Standardization to Army–Navy case sizes has resulted in the ability to front-mount the instruments and to maintain good appearance. Figure 24 shows a group of instruments so mounted.

Figure 23. Element and disassembly view of permanent-magnet-type temperature indicator

A. Operating element in frame
B. Operating element removed from frame
C. Disassembled operating element



Conclusion

Demands of the Armed Services, resulting from a tremendous advance in aviation during the present world conflict, required consideration of many varied problems by instrument engineers. Influences of wide ranges of operating temperatures, extreme conditions of shock and vibration, extension of altitude ranges, worldwide climatic conditions, adoption of new power supplies for aircraft, standardization of mechanical features, and the requirements of servicing are successfully met by the instruments now available.

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Progress in Impulse Testing of Transformers

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Synopsis: Impulse testing of transformers has resulted in improvement of design and manufacturing methods. Not only should the test methods now given in American Standard *C*57.2 be simplified to effect a reduction in cost of tests, but the test code should be modified to give an adequate assurance of failure detection. The importance of failure detection and means available at present are discussed at length. A new method is proposed which is more sensitive than the older methods and appears to be less subject to individual interpretation.

IMPULSE testing began on a commercial basis in 1933[1] when the first rules were formulated as to its conduct. Previous to this, many impulse tests had been made on a developmental basis.[2,3] Since then considerable progress has been made[4,5] in equipment and procedure, resulting finally in the rules prescribed in the present American Standard *C*57.

Failure Detection

The American Standard *C*57.2 covers the problem of failure detection as follows:

> "10.122 **Detection of Failure During Impulse Test.** Because of the nature of impulse-test failure, one of the most important matters is the detection of failure. There are a number of indications of insulation failure. Some of these are: noise within the transformer; presence of smoke; excessive current or drop in voltage in the excitation circuit as indicated by the magnetic oscillograph; failure of the gap or bushing to flash over although the oscillogram indicates a chopped wave; presence of oscillations or other variations from the expected wave shape as indicated by cathode-ray oscillograms."

The General Electric Company's experience over 11 years of commercial impulse testing on approximately 300 power transformers, of a total of 3,500,000-kva capacity, plus numerous tests on transformers for design purposes, indicates certain facts applicable to the types of transformer tested, facts which, in general, may be applied to the testing of all power transformers.

If, when testing a transformer, the indications cited in American Standard *C*57 for failure detection do occur, then it is quite certain that a failure took place. However, their absence does not prove that a failure did not occur. As a matter of fact, the absence of such indications as noise, bubbles, smoke, or excessive normal-frequency current, may produce the false impression that the transformer passed the test safely, whereas failure may have occurred.

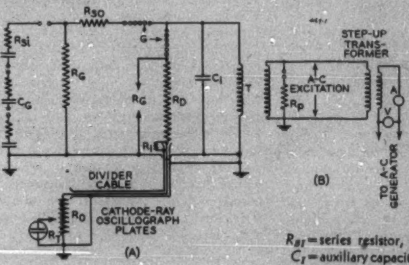

Figure 1. Impulse-test circuit for commercial transformers

A. Impulse circuit

B. Normal-frequency circuit

C_G = capacitors of impulse generator
R_G = main shunt resistor
R_{SO} = series resistor, external to generator
R_{SI} = series resistor, internal to generator
C_I = auxiliary capacitor to control wave shape
G = protecting gaps (high-vacuum)
RG = test gap (rod gap)
R_D = main resistor of voltage divider
R_I, R_O, R_T = auxiliary resistors of voltage divider
R_P = protecting resistor
T = transformer under test

Transformer failures fall in three classes:

1. Failure of major insulation to ground.
2. Failure across relatively large portions of the winding, not involving ground.
3. Failure between small portions of windings, such as coils, sections, and turns.

Table I shows the probability of a failure being detected by the methods now in use. Figures 2, 3, 4, 6, and 7 show cathode-ray oscillograms where failure is easily identified.

Detection of Failures Where Noise, Smoke, Bubbles, or 60-Cycle Follow Current Do Not Occur

Many of the failures are of this type. Most of these can be detected by a meticulous comparison of the wave shapes, taken with the cathode-ray oscillograph, of the reduced-voltage full wave and the full wave. Figures 8 and 9 show how even the smallest changes are significant. Even a failure across ten per cent of the winding results only in minor changes in wave shape. Very often differences in wave shape are detectable only if the oscillograms are superimposed on each other, making a direct comparison possible. It is clear that only a person trained to read these oscillograms is capable of making the proper interpretation.

The changes in wave shape produced by failures are caused by changes in current flow into the transformer due to a fault, resulting in consequent changes in current flow through the resistance divider (see appendix) which is used to measure the impulse voltage applied to the transformer. The success of failure detection by means of comparison of oscillograms, therefore, depends on the relative amount of current flowing into the transformer winding and through the resistance discharge paths of the impulse generator (Figure 1). It is possible to set up an impulse circuit of such characteristics that the transformer current represents only a small portion of the impulse generator current. In such a case the variations of transformer current due to a fault are so small that a visible or detectable change in wave shape between the check wave and the full wave may not occur.

This point is of considerable interest, because American Standard *C*57.2, paragraph 10.116*f*, states in part: "The wave should be as smooth as possible, because oscillations have the effect of increasing the stress on the winding under test." It can be shown that, if an effort is made to smooth out the applied wave, as shown in Figure 10, the small failure of one per cent of the winding cannot be detected, whereas, when the oscillations are present, the change in wave shape, even though it is small, will indicate the failure. The smaller the transformer kilovolt-amperes, the lower usually will be the impulse current through the winding in proportion to the total current, and the greater will be the difficulty of detection of small failures.

From this point of view, it is important that the series resistance between impulse generator and the transformer be fairly high, the impulse-generator capacitance relatively low, and the divider resistance relatively high. With this kind of loose

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coupling between generator circuit and transformer, the test wave is not likely to be smooth. However, due to greater proportionality between transformer impulse currents and purely impulse-generator circuit currents, changes in transformer currents will record on the voltage oscillogram taken at the transformer terminals. For other purposes of testing, stiff coupling, involving large generator capacitance, low series resistance and low divider resistance, are usually preferable. In testing high-impedance transformers, a compromise is therefore necessary.

A comparison of wave shapes will lead to correct conclusions only if the wave shape is not disturbed by other factors. The most important of these are: effect of normal-frequency excitation, impedance changes in transformer circuit, and the time interval which occurs between application of successive waves.

Effect of Normal-Frequency Excitation and Impedance Changes on Comparison of Oscillograms

As is explained in the appendix, gaps have to be used to separate the normal-frequency excitation from the impulse generator. Figure 11 shows a full wave where marked disturbances are seen at 13 microseconds and 39 microseconds. These disturbances are due to the separating gaps, and they may obscure an existing fault. Repetition of the wave without excitation and with short-circuited gaps is required to show that the winding has not been damaged.

Application of normal-frequency excitation permits grounding of one terminal of each winding only. As a result of magnetic and electrostatic coupling between windings,[7,8] terminals which cannot be grounded may reach potentials in excess of the standard full-wave crest applicable to that winding. This requires protection of such terminals to limit the crest voltage to 90 per cent or less of their standard full-wave level (see appendix).

American Standard C57.2, paragraph 10.118*b* specifies:

> "All terminals not being tested shall be grounded or protected by a gap or other appropriate means such as a lightning arrester."

So long as the gap or the lightning arrester do not function, the circuit conditions are not disturbed. If, however, a gap arcs over during the full-wave test, the impedance of the transformer winding under test may change considerably from the open-circuit condition, so that the reduced full wave and the full wave do not agree. Any failure occurring simultaneously within the winding, or from winding to ground, may be obscured.

If the lightning arrester operates, even for both the reduced full wave and the full wave, there will be a difference in its impedance causing a slight change in applied wave shape (Figure 5, waves 1 and 4) which, in turn, may obscure a simultaneous failure.

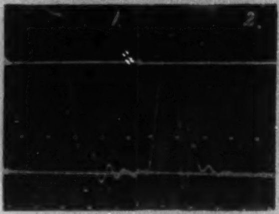


Figure 2. Failure from line end to ground indicated by second chopped wave 2. First chopped wave 1 shows correct wave shape

Obviously, the only certain way to eliminate such wave-shape differences is the use of constant impedances connected from terminals to ground of such value that the potential of the terminals is kept below the flashover voltage of the protective gap. Resistances have been used and are ideal, because their impedance will be the same, regardless of the applied impulse voltage. Arrester resistance material, such as Thyrite, is preferable from the practical point of view, when normal, frequency excitation is applied, because of its rugged construction and greater capability of withstanding normal-frequency follow current. The number of Thyrite disks then is adjusted according to the applied voltage crest, thus retaining the same impedance. The protective impedance usually must be connected to the transformer terminals by means of separating gaps which are liable to produce disturbances on the wave shape as previously discussed.

Effect of Time Interval Between Chopped and Full Wave on Failure Detection

From theory and experience, it can be shown that failures between small portions of the winding near the line end are due principally to high stresses produced by the rapid change of voltage at the line terminal. In the case of the standard impulse test, the most rapid rate of voltage change occurs on the tail of the chopped waves. The voltage in this case drops from crest to zero in a time of 0.1 to 0.2 microsecond. Therefore, depending on the chopped-wave voltage, the rate of change of voltage is of the order of several hundred to several thousand kilovolts per microsecond. This is in contrast to the rate of rise on the front of the wave which is roughly one tenth as high.

The effect of the wave tail on the

Table I. Effectiveness of Available Indicators for Failure Detection

			Indicators			
Type of Failure	Noise**	Smoke and Bubbles†	Normal-Frequency Current and Voltage‡	Chopped-Wave Oscillogram§	Presence of Variations in Wave Shape§	Type of Change in Wave Shape Shown in Figure
1. Failure of major insulation						
(*a*). Near line end to ground	Probably	Possible	Likely	Yes	Yes	2 and 3
(*b*). Within the winding to ground	Possible	Possible	Doubtful	Possible	Yes	4 and 5
2. Failure across relatively large portion of the winding	Possible	Possible	Doubtful	Possible	Yes	6 and 7
3. Failure across small portions of windings						
(*a*). Coils and sections	No	Unlikely	No	Possible	Yes	8 and 9
(*b*). Single turns	No	No	No	No	No*	

* If one turn represents less than one per cent of the winding, variations in wave shape of the present standard wave probably will not be noticeable.

** Noise is the result of explosive action of the impulse spark in the tank and depends a great deal on the amplitude of the impulse current and the length of the spark path. Consequently, failures of the kind classified as 1*a* may be accompanied by considerable noise. For the other failures, noise will be less audible and for those listed as 3*a* and *b*, noise will be absent.

† Smoke and bubbles are always produced by a failure. Whether they can be observed or not depends on the mechanical construction of the transformer. They are easily trapped in the insulation structure of high-voltage transformers and gradually absorbed by the insulating liquid.

‡ The probability that normal-frequency current will follow through the impulse failure path increases as the normal-frequency potential applied across the insulation that has failed approaches the breakdown strength. Therefore, follow current can be expected most readily in the case of failure to ground near the line end (1*a*). However, even in the case of impulse flashover of the bushing, normal-frequency follow current often does not occur. For the other failures listed, the probability of follow current is rapidly diminished.

§ Comparison of cathode-ray oscillograms detects easily failures of the type 1*a* (Figures 1, 2, 3, 6, and 7). For most of the other failures, meticulous care has to be taken to identify failures. The indications available on the oscillograms may be easily overlooked (Figures 5, 8, and 9).

stresses near the line end of a winding is illustrated on Figure 12. A comparison between stresses shown by curve 1 for the slow front of 1.5 microseconds, and the curve 4 for the chopped tail of 0.1 microsecond, gives about four-times higher stresses produced by the sudden voltage reduction.

The fact that the tail of the chopped wave is responsible for coil-to-coil failure can be proved by cathode-ray oscillograms of the kind shown in Figure 13 (1–4). That failure was caused after and during the tail of the chopped wave is evident from the fact that on the full wave (4) at 15 per cent lower voltage than was applied for the chopped waves (2 and 3) failure begins to show on the rising front, although the previous chopped wave (3) did not indicate failure of any kind. The most probable occurrence is the condition of Figure 14 (5, 6, and 7) where failure is produced on and after the tail of the first chopped wave, being indicated on the rising front of the second chopped wave.

In a comparison of wave shapes, it is most significant that a failure can only be detected, if failure which was produced by a previous wave and not detectable there, such as on the tail of a chopped wave, recurs on application of a subsequent wave. The possibility of recurrence depends on several factors, principally:

1. The type of insulation breakdown—whether solid, oil, or a combination of solid and oil.
2. The relative magnitude of stresses produced at a particular point by the wave causing breakdown and the following wave.
3. The time elapsed between application of the two waves.

The number of possible combinations is obviously very great. However, in the case of a power transformer, there is practically always a combination of solid insulation and oil, insofar as turn and coil insulation are concerned. As has been pointed out, major failures are readily detectable at the time of failure, and we need not be concerned with that type of failure at this time: for instance, where bare metal from bushings or ratio adjusters may arc to a tank.

However, the relative amount of solid and oil will change. It can easily be shown that, the greater the relative amount of solid that is punctured, the smaller the subsequent applied gradient needs to be to reproduce failure. On the other hand, if the relative amount or strength of solid in a given failure path is very small, it is conceivable that, with a slightly lower gradient produced by a subsequent wave, failure will not recur, provided the time interval between the two waves is long enough to allow replacement of the broken-down oil with new oil of dielectric strength equivalent to the oil present during the initial failure.

The only example where this phenomenon was observed is shown on Figure 14 which shows a reduced full wave—1, a full wave—2 (with power and possible interference of the separating gaps and protective circuits on other bushings). Fifteen minutes later, another full wave—3—without power, was applied showing a complete check with wave 1, indicating that failure did not occur during application of this wave. It was necessary to apply a further chopped wave, immediately followed by a full wave to establish the fact that the winding had failed.

The time factor, therefore, is important. The allowable time depends not only on the relative amount of solid and oil, but also on the flow of oil past the failure. Thus, if the failure occurs in a place where oil circulation is considerably restricted, the allowable time may be fairly long. On the other hand, if the transformer oil is hot, and a considerable free circulation of oil occurs on this account, only a very short time may be required to clear the fault.

It appears, therefore, of importance that the test code include a time limit between application of the last chopped and the final full wave.

Failure Indication by Normal-Frequency Power-Follow Current

Normal-frequency oscillograms synchronized with impulse waves always show one-half cycle of distortion, Figure 3, right, and Figure 15. This distortion will be different with different transformers tested, since it is caused by the impulse current flow into the low-voltage normal-frequency circuits, such as step-up transformers, generators, and connected protective devices superimposed on the power-current flow into the impulse circuit. Both of these transient currents depend greatly on the impedance of the tested transformer and of the connected circuits. Therefore, the power waves do not even appear the same if different windings of the same transformer are tested.

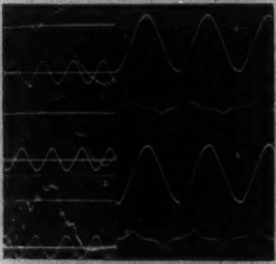


Figure 3. Failure of bushing by creepage

Wave 1—Correct chopped wave
Wave 2—Failure during chopped wave
Wave 3—Failure during following full wave
Wave 4—60-cycle voltages (top) and current (bottom) taken simultaneously with wave 2
Wave 5—Same as 4 after bushing was repaired

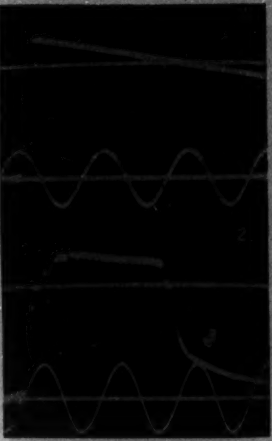


Figure 4. Creepage failure to ground from a point about 12.5 per cent from line end

Wave 1—Reduced-voltage full wave
Wave 2—Failure during attempted full wave

The magnetic oscillogram indicates failure only if the power-current disturbance lasts longer than one-half cycle. Even then, the separating gaps or connected protective devices may be at fault in not sealing off properly after one-half cycle of operation.

If power follow does occur through a fault established by the impulse, this current must persist for longer than a cycle or at least longer than one-half cycle in order to indicate failure positively by the magnetic oscillogram. Further, the spark-over of a rod gap during the chopped-wave test or a protective gap on a bushing not being tested directly may cause power-follow currents greatly in excess of a power-follow current simultaneously occurring in a small fault within the winding. The experience can be summarized thus:

While in a few cases power-follow current has occurred after an impulse failure as indicated by magnetic oscillograms of normal-frequency voltage and current, there has not been a single case where failure was indicated by increase in normal-frequency current where the failure was not also detected by comparison of wave shapes obtained with the cathode-ray oscillograph. On the other hand, there have been many cases where the cathode-ray oscillograph indicated failure while the magnetic oscillograph did not give the least evidence. Magnetic oscillograms in Figure 3 and Figure 15 are included to show this fact. In the case of Figure 3, follow current did not occur although the line bushing failed internally to ground. Figure 15 indicates the type of distortion obtained in the normal-frequency wave when chopped waves and full waves are applied.

Waves 2 taken during failure and 3 when no failure occurred are identical. Waves 2 were taken simultaneously with the full wave 2 of Figure 8.

The principal virtue of normal-frequency excitation is that, if follow current flows, the failure point within the winding may be found more easily after the winding is disassembled because of the greater burning. On the other hand, the transient analyzer[9] has been used successfully to locate almost the exact point of failure. This is done by creating faults at various points in the winding until the point is located where the applied wave matches closely the impulse fault wave. This can be done after the transformer is untanked, and obviates the necessity of normal-frequency current for this purpose. Where this instrument is not available, power can be applied after failure has been detected by other means to burn down the fault. In most cases, however, many impulses must be applied before the insulation is sufficiently weakened to allow normal-frequency follow current to flow.

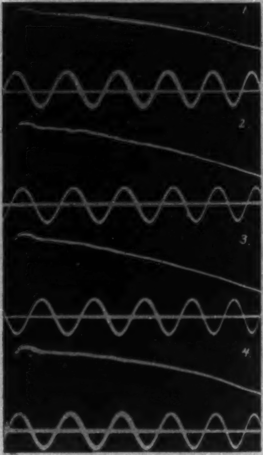


Figure 5. Effect on wave shape when failures occur to ground far removed from line end (staged test)

Wave 1—Reduced-voltage full wave

Wave 2—Failure from mid-point to ground

Wave 3—Failure from 75 per cent from line to ground

Wave 4—Arrester operation on low-voltage winding

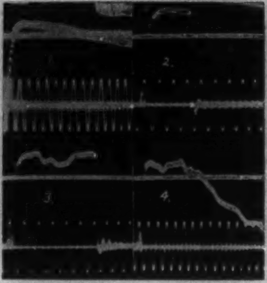


Figure 6. Failure from line end to point 20 per cent from grounded end of winding

Wave 1—Reduced-voltage full wave

Wave 2—First chopped wave

Wave 3—Second chopped wave indicating beginning of failure

Wave 4—Attempted full wave definitely indicates failure

Summary of Failure-Detection Problem With Present Available Methods

1. Most but not all impulse failures are detectable by present methods.

2. Meticulous comparison between reduced-full-wave and full-wave oscillograms, by directly overlapping, will detect many failures of which no other evidence is present.

3. Normal frequency excitation will occasionally confirm a failure. However, its use may cause distortion of the impulse-wave shape and thus obscure a failure otherwise detectable from the cathode-ray oscillograms.

4. Minor changes in the wording of the American Standard *C57.2* test code are suggested by this experience to emphasize the importance of the wave-shape comparison as a failure indicator. These are being submitted to and for consideration by the electrical machinery committee of the AIEE.

New Method of Failure Detection

A new method of failure detection is being studied which has made it possible to detect the failure of one turn in a winding of 1,350 turns. This method consists in comparing the shapes of the impulse current waves through the neutral of the transformer winding recorded at the time of application of the reduced and final full wave, respectively. This method appears to be extremely sensitive to small changes in impedance of the transformer winding.

The method requires the simultaneous use of two cathode-ray oscillographs: one to measure the applied voltages required by the American Standard, the other to measure the neutral current.

As an example, comparison of the current waves in Figure 16 clearly shows the difference in wave shape produced by even one short-circuited turn, or 0.077 per cent of the winding. For larger failures, the differences become even more pronounced.

Figure 17 shows the impulse voltage and current waves and magnetic oscillograms when a sample turn insulation was broken down (subscript *F*). The sample was connected across ten turns, or 0.75 per cent of the winding. These waves are compared with similar waves when breakdown did not occur (subscript *O*).

The figure shows:

1. The magnetic oscillograms do not show the slightest change in current-wave shape due to failure. The half cycle of follow

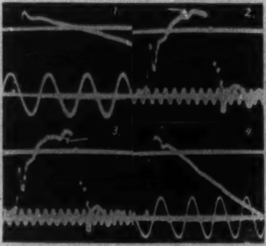


Figure 7. Failure from line end to a point 75 per cent from grounded end of winding

Wave notation same as in Figure 6. Failure indicated on first chopped wave

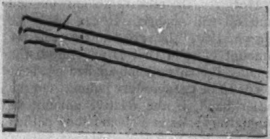


Figure 8. Failure almost not detected

From top to bottom:

Wave 1—Reduced-voltage full wave

Wave 2—Full wave with power. Small depression in wave near point of arrow suspected

Wave 3—Full wave without power indicates failure current represents 60-cycle discharge principally through the resistance divider and into the impulse generator.

2. The voltage waves are so nearly alike as to lead to the belief that the transformer successfully withstood the test.

3. There is absolutely no question that failure occurred when the current waves are compared.

This example strikingly shows the possibilities of this new method as a failure detector. The war has interrupted the complete evaluation of all the factors involved. Before adopting such a method, it will be necessary to investigate it thoroughly and particularly to find its limitations if they should exist.

This method is effective, regardless of the type of impulse generator used, since it measures the impulse current through the transformer winding directly. However, similar to the voltage-wave comparison method, it is subject to interference by normal-frequency excitation and changes in protective impedances. However, it appears that this method, with necessary precautions and experience, may solve the problem of failure detection, thus greatly enhancing the value of the impulse test.

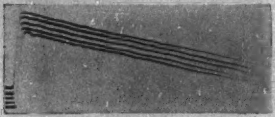


Figure 9. Comparison of applied wave shape with failure across different portions of a winding (staged test)

From top to bottom:
Wave without failure
Wave with failure across 1.9 per cent of winding
Wave with failure across 4.2 per cent of winding
Wave with failure across 4.2 per cent of winding
Wave with failure across 10.1 per cent of winding

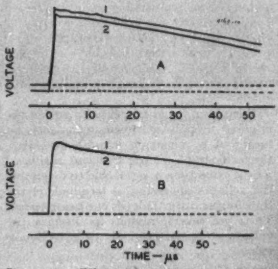


Figure 10. Effect of test circuit on possibility of failure detection

A. Loosely coupled circuit—failure changes wave
1—Correct wave
2—Wave after failure

B. Closely coupled circuit—failure cannot be detected. Failure does not change wave sufficiently

Complexity of Test

Provided that all failures can be detected, the impulse test is a very valuable test to establish the insulation level of a transformer. However, the American Standard Association standard tests at present are of such nature that it is impractical to make this test on all units manufactured. These require: a reduced-voltage full wave, two chopped waves, and a full wave at specified voltage levels and synchronized with the normal-frequency voltage crest of polarity opposite to the impulse polarity. These four waves are to be applied to each bushing separately. If, on a three-winding three-phase transformer, all windings are to be tested, this means nine bushings with the connected winding must be tested individually with probably three different setups of impulse-generator circuit and winding protection. The result of the test is not known until the oscillograms which record the impulse waves are developed. Approximately four development periods are required per winding to assure that the proper crest voltages are obtained and thus avoid repetition of voltage application due to insufficient potential or repeatedly overstressing the winding beyond the required test voltage.

The total testing time due to all reasons previously shown is, therefore, considerably in excess of applied potential and induced-voltage tests, where a whole winding or the whole unit, respectively, is tested at once, while the applied voltage is determined by a direct-reading voltmeter.

The impulse test on such a transformer may require as much as 20 hours of testing time. It is evident that as a standard testing procedure the expense of such test is considerable, not only on account of the relatively long time required to make the tests, but also because of the specialized equipment and highly trained personnel required. In order to make impulse tests on all transformers which may be subjected to lightning waves, it would be necessary with present impulse specifications to enlarge the impulse-testing facilities many fold, thus adding considerably to the final cost of the transformers.

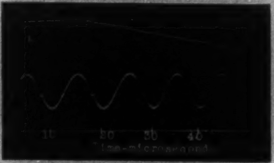


Figure 11. Example of distortion of the applied impulse-voltage wave caused by separating gaps between impulse and 60-cycle circuits at 13, 39, and 52 microseconds

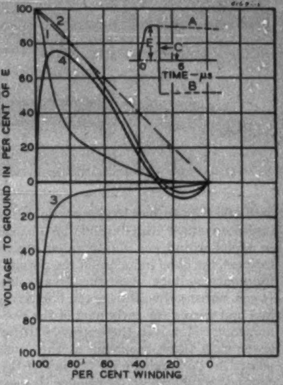


Figure 12. Voltage distribution in a transformer winding due to full and chopped waves

A—Positive full wave 1.5x40
B—Negative full wave 0.1x40
C—Resulting chopped wave
1—Initial voltage distribution due to wave *A*
2—Voltage to ground at three microseconds—wave *A*
3—Initial voltage distribution due to wave *B*
4—Resulting voltage as wave C becomes zero

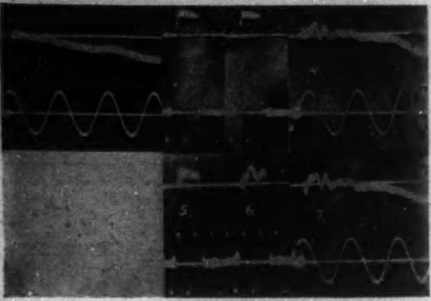
Certain changes are being submitted to and for consideration by the electrical machinery committee of the AIEE, which would help considerably in reducing impulse-testing time. The most effective of these is omission of normal-frequency excitation which would

1. Eliminate the setup time of connections between normal-frequency source and transformer under test.

2. Do away with separating gaps, thus eliminating repetition of impulse waves because of distortion due to gap discharge.

3. Simplify the protective equipment on bushings other than the one being impulsed. In many cases, most of the bushings could be grounded directly. Consequently, less time is required to ascertain that failure has not occurred.

The omission of normal-frequency excitation may modify the stresses at some points in the winding, the amount depending on the impulse-voltage distribution characteristics of the transformer. In the



Top: Reduced-voltage full wave, two chopped waves, full wave indicating failure beginning at the front of the wave although amplitude is 15 per cent lower than previous chopped wave

Bottom: Failure caused by steep tail of first chopped wave evidenced by breakdown on front of second chopped wave

Figure 13. Demonstrates that the steep tail of the chopped wave causes failure across small sections of winding near line end

case of a transformer having straight-line impulse-voltage distribution, no change in stress would occur. In other transformers, the chief influence of the normal-frequency voltage is on the stresses between turns and coils near the line terminal and during the early part of the phenomenon. However, these stresses are far lower than those occurring at the same points in the windings on the tail of the chopped wave, where the change, if any, due to omission of the normal-frequency excitation is insignificant.

Therefore, the effect of the normal-frequency excitation on the maximum stresses in the transformer during impulse tests is of the same order as that of such variables as the length of the applied wave front, the shape of the wave, and the crest voltage (specified on a minimum basis).

It appears, therefore, that serious consideration should be given to the omission of the normal-frequency excitation during impulse tests, not only because of the important saving in time, but because it would open the way to precise fault detection by means of the cathode-ray oscillograph. With the present test methods, the risk of passing a faulted transformer is so great as to detract seriously from the value of the test.

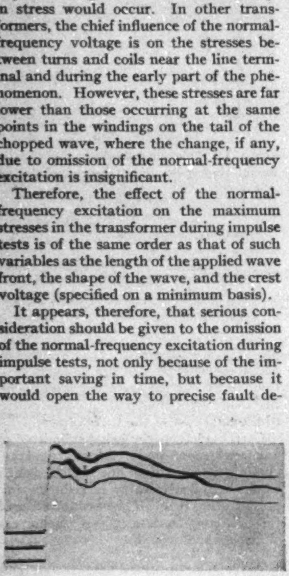


Figure 14. Delay between application of chopped and full waves can clear up faults caused by tail of chopped waves and remain undetected

From top to bottom:

1—Reduced full wave followed by two chopped waves not shown but not indicating failure

2—Attempted full wave with power does not check, but change may be caused by separating gap or impedance connected to other bushings

3—Full wave without power taken 15 minutes after wave 2 checks perfectly wave 1 although transformer had really failed

Conclusions

1. Commercial impulse testing has made a distinct contribution toward the design of transformers for anticipated service conditions.
2. It has in some cases disclosed defects in materials and manufacturing that other tests would have failed to detect.
3. It is suggested that the test code be modified to give adequate assurance of failure detection.
4. The test procedure and equipment should be simplified to reduce the cost of testing.
5. Failure detection can be improved greatly, and the cost of the test can be reduced substantially if normal-frequency excitation is omitted.

Appendix. Outline of the Test Circuit and Test Procedure

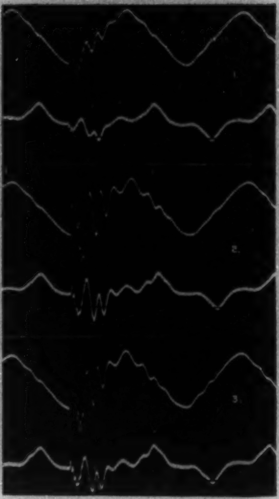


Figure 15. Magnetic oscillograms taken during tests with transformer which failed

1—Example of distortion caused by chopped impulse wave in the applied normal-frequency voltage and current

2—Same as 1 for full-wave application simultaneous to wave 2, Figure 8

3—Same as 2 except on repaired transformer. Failure did not occur

The impulse test is a relatively new test and is more complicated than the older-type high-potential low-frequency tests. It requires for the production of the impulse wave the so-called impulse generator, which, in practically all laboratories, is a Marx generator. In such generators[10] a number of capacitors are charged in parallel to a relatively low voltage and then discharged by means of sparking over sphere gaps, which connect the capacitors in series. The voltage of the separate capacitors then adds up to the output voltage of the impulse generator. The wave shape is obtained by the interaction of the generator capacitance, series resistance, divider resistance, load capacitance, and finally the test piece itself. The calculations of wave shapes and voltages for impulse-generator circuits have been given in various papers.[11–14] In the case of the transformer calculations are extremely lengthy, especially for transformers which have more than two windings. This is due to the fact that the inductance of the winding tested is affected considerably by the conditions existing at the terminals of the other windings. Frequently the other terminals have to be protected to avoid flashover of the bushings with a consequent change in applied wave shape.

It has been found expedient to predict the required impulse-generator constants by means of a miniature impulse generator, the oscillograph electric transient analyzer.[9] At the same time it is possible to determine the voltages appearing at terminals of the transformer other than the one being tested. There are several reasons for keeping the voltage of these terminals to a minimum value:

1. It is undesirable to stress any winding not being tested; that is, one on which no direct oscillographic measurements are being made, in excess of about 90 per cent of its specified full-wave test voltage.
2. Flashover of protective gaps at such terminals frequently will modify the wave shape of the applied wave obscuring any fault which might have occurred simultaneously in the tested winding.

To avoid flashover of the terminals on other windings, it is often necessary to connect an impedance to the terminal which will modify the voltage rise at that terminal

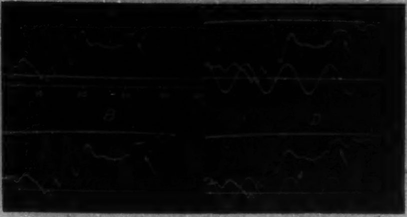


Figure 16. Impulse current waves through neutral of winding

Comparison of waves detects very small failures

A—No failure

B—Failure of 0.074 per cent of winding

C—Failure of 0.222 per cent of winding

D—Failure of 0.738 per cent of winding

and restrict it to a value below the flashover voltage of the protective gap. This impedance, in general, may be a resistor, a stack of Thyrite arrester disks, a lightning arrester, a capacitor, or a combination.

Two types of excitation are required for the test, these being:

1. Impulse excitation.
2. Normal-frequency normal-voltage excitation with proper synchronization.

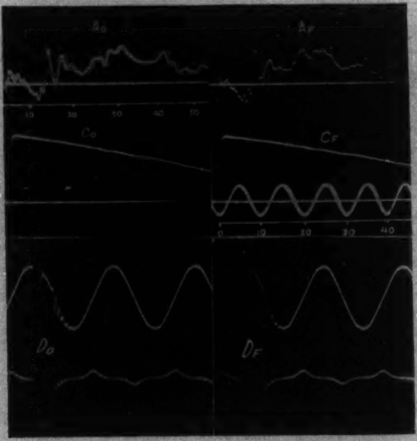


Figure 17. Comparison of failure-detection methods

A—Current wave through neutral of winding

C—Full wave applied

D—Magnetic oscillogram of 60-cycle voltage (top) and current (bottom)

O—No failure

F—Failure occurred

The application of two types of excitation imposes complications, because it is impractical to build impulse equipment capable of withstanding the normal-frequency excitation currents. The impulse circuit, therefore, must be separated from the transformer by means of gaps, or a combination of fuses and gaps which will permit flow of normal-frequency currents through the impulse circuits only after the impulse has been applied, and then only for one-half to one cycle. Different types of gaps are being employed for this purpose.

The Pittsfield laboratory utilizes a high-vacuum gap with properly spaced electrodes. This gap is capable of sealing off impulse current even before it reaches current zero. Other suitable gaps are expulsion protector tubes alone or in combination with fuses. Sphere gaps with directed air blast also are employed for this purpose.

Circuits used for transformer impulse testing have been described in the literature.[14,15] The principal constants of the circuit of the Pittsfield laboratory are shown on Figure 1. This figure is largely self-explanatory and comprises the most complete circuit arrangement. Usually RG is omitted. The connection of the divider R_D as shown is made to assure that the wave measured actually is applied to the transformer.

The impulse voltage is measured[16,17] by means of the resistance divider R_D which reduces the amplitude of the applied impulse voltages to values which can be applied to the cathode-ray oscillograph plates.[18,19] The normal-frequency currents and voltages can be measured with an ordinary type of magnetic oscillograph.

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The Effect of Weather on the System Load

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THE system load of an electric power company may be regarded as having several components. Firstly, there is a fixed component of the system load that may be considered as reflecting the business activity of the territory of the company. Secondly, there is a variable component of the load that reflects the effect of the weather. Thirdly, there is a component of load, relatively infrequent in occurrence, that reflects the public response to events of unusual attraction. There is the positive effect on the load of the appeal of radio broadcasts of great local or national interest and the negative effect that evidences the influx of the buying public into shopping areas which reaches its peak in the Christmas period.

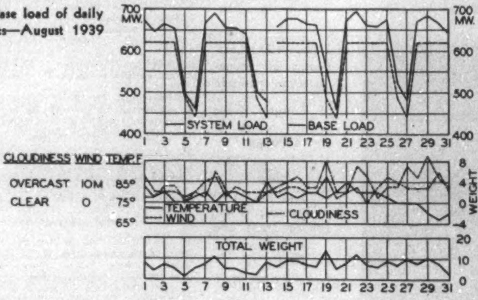

Figure 1. Base load of daily system peaks—August 1939

The fixed component of the system load is a definite value for a particular peak period or a particular hour of the day. It is a base load to which the variable component of the load is applied to form the system load. In Figure 1 the base loads of the daily system peak of August 1939 are indicated and the difference between the base and the peak is the variable component that reflects the effect of the weather conditions, the weight of which is also shown.

The separation of the system load into its usual two components, that of the fixed value or base load, and that of the variable quantity which reflects the effect of the weather, serves to indicate that the estimating of the system load, in effect, is forecasting the weather and giving each element the correct weight to obtain accuracy in the load estimate. In Figure 2, this weighting of the weather is indicated.

When the fixed value or base load is correct and the weighting of the weather conditions is precise, then a further load increase in response to a radio broadcast having appreciable appeal, or even a load decrease, can be determined within a reasonable degree of accuracy. As an instance, the increase of load from the radio broadcasts of the two football games of Saturday, November 20, 1943, one of local interest between Temple and Villanova and the other between Iowa Seahawks and Notre Dame, is indicated in Figure 3. A decrease in the load as occasioned by the reduction in residential demand as the buying public crowds the shopping centers of the territory of the company as exemplified during the Christmas period is shown in Figure 4.

Base Load

If the effect of the weather on the system load, properly weighted, is to be observed in the load estimate, there must be a base load to which the weather weight may be applied.

The base load or as it has been termed, the fixed component of the system load, may remain the same for a particular hour of a midweek day for an appreciable period of time. Any change in the base load that may occur can be attributed to a change in business conditions, to a change in the time of approaching darkness, or to a change in the temperature base from which the weight of the temperature in its effect on the system load is calculated.

Any change in the temperature base will be according to plan and any change in the time of approaching darkness will be known in advance. If there has been no change in these two factors, then any variation in the base load will reflect a change in business activity in the territory supplied by the company.

Although the system load is usually greater than the base load, it is possible for the reverse condition to occur. For instance, on a day when there is little wind and a clear sky, the temperature may have a negative weight and the net combined weight may also be negative. In this instance, the system load will be less than the base load. This condition may be observed in Figure 3.

DETERMINATION OF THE BASE LOAD

The determination of the base load is a matter of weighting the temperature, the degree of cloudiness, and the wind velocity, and applying the combined weight as a percentage to the system load. It is possible that the effect of humidity should also be weighed but the present practice of weighting the three elements works satisfactorily.

In applying the combined weight as a percentage to the system load to determine the base load, the weights as given in Table I would be applicable to the weather conditions noted as prevailing. If the system load were 1,120 megawatts, the base load will be (1,120÷1.11) = 1,009 megawatts.

Initially, the proper weighting of the elements of the weather will be attained only after a period of "cut and try" measures, and finding that the base load obtained is the same for any particular hour of the midweek days of a particular week. For several consecutive Saturdays or Sundays, the base load of a particular hour should be the same.

Table I. Weighting of the Weather

Temperature	40 F	Weight	4
Wind velocity	Eight miles	Weight	3
Cloudiness	Overcast	Weight	4
		Total weight	11

If the hour for which the calculation is being made is that of approaching darkness, the base load may change with the time of approaching darkness but the percentage variation of the corresponding period of the previous year will obtain.

ESTIMATING THE LOAD

Service reliability and efficient operating performance demands accuracy in

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Paper 44-106, recommended by the AIEE committee on power generation for presentation at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944. Manuscript submitted April 13, 1944; made available for printing May 18, 1944.

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Table II. Weather-Weighting Table

Temperature, F	Wind Velocity, MPH	Degree of Cloudiness	Weight
25	25	Clouds very low and very heavy	10
30	20	Clouds very low and heavy	8
35	15	Clouds low and thick	6
40	10	Clouds high and thick	4
45	5	Cloude high and thin	2
50	0	Clear—blue sky	0
55		Clouds—reflecting sun	—2
60		Clouds and snow—reflecting sun	—4
65			—6

Table III. Monthly Temperature Base, Degrees Fahrenheit

Weights	December January February March	April November	May October		June September		July August
10	15	25	35			95	100
8	20	30	40			90	95
6	25	35	45			85	90
4	30	40	50	80	50	80	85
2	35	45	55	75	55	75	80
0	40	50	60	70	60	70	75
−2	45	55	65		65		70
−4	50	60					65
−6	55	65					
−8	60						

Table IV

Date	Base Load, Megawatts	Temperature, F	Wind Velocity, MPH	Degree of Cloudiness	Combined Weight	Load Estimate	Actual Load
10-23-42	930	60 (0)	5 (2)	Clear (0)	2	949	950
10-27-42	930	44 (6)	8 (3)	Clear (−2)	7	995	995

the forecast of the system load in order that generating capacity adequate to supply the system load and provide the necessary spinning reserve may be economically scheduled.

The scheduling of spinning reserve as coverage for the loss of the largest unit or the loss of that transmission capacity which renders unavailable the greatest amount of generating capacity is an accepted principle of reliable system operation. What may be overlooked, however, is the far greater probability of a deficit of operating reserve capacity as a result of inaccuracy in estimating the system load. In interconnection operation, where savings from reserve diversity are realized, a deficit in spinning reserve as a result of errors in load estimating occurs far more frequently than from the loss of generating capacity.

In estimating the daily system load it was usual to observe only the possible effect of cloudiness in raising the peak demand. A consequence of this practice was a frequent deficit of spinning reserve. In a few instances, there were deficits of generating capacity, and lowered frequency reduced the load to the value of the available generating capacity. It is probable that the loss of a large generator, under these circumstances, would have made load curtailment necessary.

Inaccuracies in the daily load forecast are usually the result of failure to calculate accurately the effect of weather on the system load. If the estimates are held within two per cent of the actual loads the performance may be considered satisfactory. In many instances the estimate and the actual load will be the same, but there will be some cases when rapid changes in the weather make such accuracy impossible. Such changes in weather conditions, however, are generally attended by a compensating effect in the corrected weather weights that reduces the error in the load estimate.

The schedule of generating capacity for the estimated morning peak load which usually occurs at 8:30 a.m. is prepared the previous day. Accuracy of weather forecasting so far in advance is frequently difficult. A careful review of the load estimate, however, is made the following day when the weather may be more accurately predicted.

In weighting the weather, approximately two hours in advance of the peak period, or less if generating capacity can be made available in time, the load dispatcher may observe the prevailing weather, note the trend of the forecast, and determine if the load estimate made the previous day should be revised.

Weighting the Weather

As has been indicated, the initial weighting of the weather should be preceded by a period of "cut and try" measures to determine the effect of each element of the weather on the system load of a particular electric power company.

In making an accurate load estimate, proper weighting of the degree of cloudiness, wind velocity and temperature based on the best available weather forecast, is essential. When the weather forecast is received, reference is then made to a weather-weighting table for the total

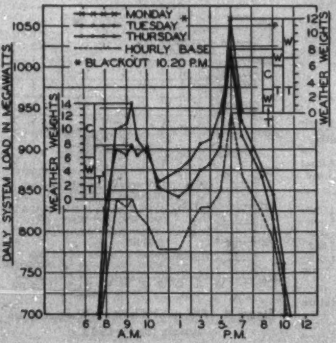
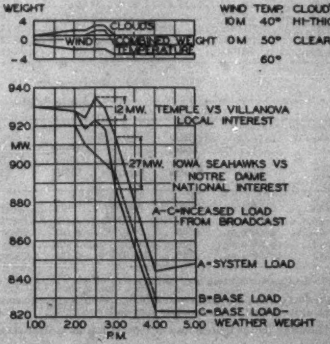


Figure 2 (left). Effect of weather conditions on system load, February 1942

C—Cloudiness
W—Wind
T—Temperature
T_1—On this day, the weight of the wind of three to five miles per hour was offset by the increased brightness of the day due to the reflection of the sun rays from scattered fair-weather clouds

Figure 3 (right). Effect on load of broadcasts

Football games, November 20, 1943



weight to be applied as a percentage to the base load (Table II).

Temperature Base

The temperature base for weighting the effect of the predicted temperature on the load is the normal mean temperature of the month as given in the Weather Bureau Monthly Summary. Thus, the normal mean temperature of the month would have Zero weight. For Philadelphia this is as indicated in Table III. For the months of May and October, and June and September, a two-column scale is used, as temperatures during these four months may be considerably lower than the monthly normal mean, thus promoting the use of heating devices, while on other days they may be appreciably higher, causing an increased demand of refrigerators and other cooling devices. There is a time lag to the customer response, however, when the temperature change tends to use of appliances other than those which had been in service and the load change does not become apparent until the abnormal temperature conditions are sustained for longer than one day.

In the change of the base temperature from the 70 degrees of June to the 75 degrees of July and August, the base load, in effect, would be increased by two per cent. As an instance, the base load of April, the base temperature of which is 50 degrees, is decreased four per cent for May with its base temperature of 60 degrees. Normally the base load of July and August, however, would be the same as June, because of the reduction of customer demand during the summer months as a result of the vacationing exodus. The foregoing implies that business conditions in their effect on the load have remained the same. The effect of gasoline rationing had to be observed this year and evaluated to arrive at the base load for these two months. It appeared that gasoline rationing had caused many people to forego summers at the shore or mountains, as there was a two per cent increase in the base during July and August of 1943.

Temperature Effect During Summer

The increase of load during the summer as the temperature increases is the result of increasing operation of fans, refrigerators, air conditioning, and other cooling devices.

The effect of temperature on the load during this period is graphically illustrated in Figure 5, which indicates the hourly system load of June 17, 1939, and June 24, 1939. The dates are those of successive Saturdays. The difference of 13 per cent in the load of these two days at 1:00 p.m. is attributable in the main to the difference in prevailing temperatures these two days. At 10:00 p.m., June 17, 1939, an electrical storm was approaching and reached its height at 11:00 p.m. The effect of silencing radios is evident at this time in a net reduction of 40 megawatts, approximately, in the system load.

Temperature Effect in Fall

The increase of load in the early fall may occur because of a rise or a drop in temperature. It is obvious that when the temperature is higher than 70 degrees the load will tend to increase as it does in summer. It is also obvious that a drop in temperature below 70 degrees will cause a reduction in refrigeration and fan demand and a continued decrease in temperature will then cause an increase of customer demand through use of space heaters, house-heater operation, heating of trains and cars, increased radiation of industrial heating equipment, and so forth.

The effect of temperature on the October system load is indicated in Table IV. On the 27th a weight of minus two was applied under degree of cloudiness to observe the lightening effect

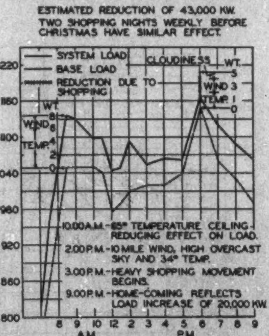


Figure 4. Effect of Christmas shopping on system load—1943

Table V

Date	Base Load, Megawatts	Temperature, F	Wind Velocity, MPH	Degree of Cloudiness	Combined Weight	Estimate	Actual Load
11- 7-38	720	70 (−8)	18 (7)	Overcast (7)	6	763	762
11-28-38	730	29 (8)	5 (2)	Clear (0)	10	810	798

Table VI

Date	Base Load, Megawatts	Temperature, F	Wind Velocity, MPH	Degree of Cloudiness	Combined Weight	Estimate	Actual Load
12- 5-38	760	58 (−7)	15 (6)	Heavy clouds (6)	5	810	801
12-28-38	750	26 (6)	10 (4)	Clear (0)	10	820	823

Table VII

Date	Time, A.M.	Base Load, Megawatts	Temperature, F	Wind Velocity, MPH	Degree of Cloudiness	Combined Weight	System Load
1-13-43	6:00	560	24 (3)	8 (3)	0	6	594
1-13-43	7:00	710	24 (6)	8 (3)	0	9	776
1-13-43	8:00	910	24 (6)	8 (3)	0	9	990
1-13-43	8:30	980	24 (6)	5 (2)	0	8	1,057
1-13-43	8:45	980	25 (6)	8 (3)	0	9	1,072
1-13-43	9:00	970	25 (6)	5 (2)	−1	7	1,040
1-13-43	10:00	960	25 (3)	5 (2)	−1	4	997

Table VIII

Date	Time	Base Load, Megawatts	Temperature, F	Wind Velocity, MPH	Degree of Cloudiness	Combined Weight	Megawatts Estimate Load	Megawatts Actual Load
2-15-43	8:30 a.m.	980	7 (13)	3 (1)	Clear (−2)*	12	1,098	1,022
2-15-43	10:30 a.m.	960	8 (7)**	5 (2)	Partly cloudy (0)	9	1,048	1,046
2-15-43	3:00 p.m.	930	10 (6)**	5 (2)	Clear (−2)*	6	986	988
2-16-43	8:30 a.m.	980	11 (11)	1 (0)	Fair (−3)***	8	1,058	1,047
2-16-43	10:00 a.m.	960	13 (6)**	5 (2)	Clear (−2)*	6	1,018	1,019

* The effect on the system load of reflection of sun's rays from blanket of snow.

** After 9:00 a.m., the temperature ceiling of 65 degrees to conserve fuel reduces the effect of temperature on the system load approximately one half.

*** The effect on the system load of reflection from scattered clouds and from a snow blanket.

of a brilliant full moon in lowering the lighting demand.

Effect of Extreme Temperature Changes

In November 1938 the temperatures varied from 70 degrees on November 7 to 29 degrees on November 28. The effect on the load of this change in temperature would have been approximately 16 per cent, or about 116,000 kw, if the wind velocity and degree of cloudiness had been the same each day. How the temperature affected the load on these two days is shown in Table V.

It is probable with the old practice of comparing loads of days on the basis of their relative cloudiness that the load-estimating error might have exceeded 100,000 kw. In the case of November 28, a wind of eight miles per hour velocity, with a weight of 3, was forecast.

Temperature Effect in Winter

The temperature base for December is 40 degrees as this is approximately the normal mean temperature of the winter months in Philadelphia.

Temperatures in December 1938 varied from 58 degrees to 26 degrees, a difference in weight of approximately 13 per cent, or approximately 100,000 kw. How this difference in temperature affected the load is indicated in Table VI.

Effect of Temperature Ceiling

During the winter of 1942, a temperature ceiling of 65 degrees in the homes and office buildings was established in order to conserve fuel oil. This reduced the temperature effect on the load approximately one half during the greater part of the day, although over the morning peak period, up to 9:00 a.m., the effect was about normal. Table VII is given as an example.

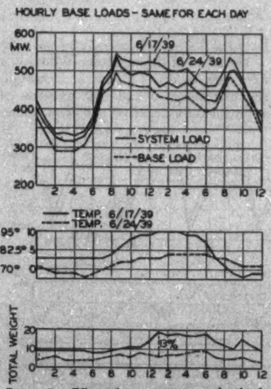


Figure 5. Effect of temperature on load of Saturdays in June 1939

Saturation Effect of Extremely Low Temperatures

What is the effect on the system load of extremely low temperatures? Does saturation occur and diversity disappear in the use of heating equipment at a particular temperature, or is the high load estimate the result of lowered industrial production because so many workmen who rely on automobile transportation are unable to start their cars or finding the icy highways too great a hazard, fail to report for work? In February of 1943 temperatures reached extremely low values and Table VIII indicates the effect.

It may be said that the high estimate of 8:30 a.m., Monday, February 15, 1943, reflects the effect of frozen automobiles and icy highways as the system load of the late morning and afternoon are normal. It is probable, however, that diversity in the use of heating equipment will tend to diminish appreciably as the temperature is reduced below zero.

Cloudiness

The change in customer demand due to cloudy weather appears to vary in amount in direct proportion to the degree of cloudiness. The scale of the weights as used for different degrees of cloudiness is given in Table IX. The weight to be applied for the degree of cloudiness is in proportion to the lowness and the thickness or heaviness of the cloud masses.

Effect of Cloudiness on Day Load

The effect of cloudiness on the day load is indicated in the weighting of the weather factors on the peak loads of May 23 and 24, 1939. On the morning of May 23, there was a dense fog that persisted for several hours, causing an exceptionally heavy peak demand at 9:15 a.m. The normal peak hour for the day was at 8:30 a.m. (Table X).

Effect of Cloudiness at Dusk

The effect of heavy clouds in causing the more rapid approach of darkness is such that additional weight must be given this factor in estimating the load for the hours in advance of the period of normal darkness. The added weights and the hours for which these are applicable are:

First hour in advance of normal darkness—add 4.

Second hour in advance of normal darkness—add 6.

Third hour in advance of normal darkness—add 3.

Table IX

Degree of Cloudiness	Weight
Fair: Broken fair-weather clouds reflecting sun	—2
Fair: Scattered fair-weather clouds reflecting sun	—1
Clear: Blue sky	0
Fair: Thin haze	1
Fair: Scattered clouds high and thin	1
Fair: Thick haze	2
Scattered clouds—low and thick	2
Broken clouds—high and thin	2
Broken clouds—low and thick	3
Overcast sky—clouds high and thin	3
Overcast sky—clouds high and thick	4
Light fog	4
Overcast sky—clouds moderately low and thick	5–6
Overcast sky—clouds low and heavy	7
Overcast sky—clouds very low and heavy	8–9
Moderate fog	8
Overcast sky—clouds very low, very heavy	10–12
Dense fog	10–12

Table X

Date	Time, A.M.*	Base Load, Megawatts	Temperature, F	Wind Velocity, MPH	Degree of Cloudiness	Weight	System Load, Megawatt
5-23-39	8:30	620	66 (−2)	10 (4)	Dense fog (8)	10	684
5-23-39	9:15	600	68 (−1)	15 (6)	Very dense fog (12)	17	704
5-24-39	8:30	620	67 (−1)	18 (7)	Fair (1)	7	663
5-24-39	9:00	600	67 (−1)	15 (6)	Clear (0)	5	631

* Standard time.

Table XI. August 29, 1939

Time, P.M.*	System Load, Megawatts	Temperature, F	Wind Velocity, MPH	Cloudiness	Weight	Added Weight	Base Load, Megawatts
1	660	68 (−3)	20 (8)	Rain (8)	13		585
2	651	68 (−3)	20 (8)	Rain (6)	11		585
3	639	68 (−3)	20 (8)	Rain (6)	11		575
4	657	67 (−3)	20 (8)	Rain (6)	11	+3	575
5	606	65 (−4)	18 (7)	Rain (6)	9	+3	540
6	537	65 (−4)	10 (4)	Rain (5)	5	+6	485
7	593	65 (−4)	18 (7)	Overcast (5)	8	+4	530
8	594	65 (−4)	18 (7)	Overcast (4)	7		555
9	583	65 (−4)	18 (7)	Overcast (4)	7		545
10	499	65 (−4)	12 (5)	Partly cloudy (2)	3		485
11	448	65 (−4)	10 (4)	Partly cloudy (2)	2		440
12	398	65 (−4)	8 (3)	Partly cloudy (1)	0		400

* Standard time.

Table XII. June 13, 1939

Time, P.M.*	System Load, Megawatts	Temperature, F	Wind Velocity, MPH	Cloudiness	Weight	Added Weight	Base Load, Megawatts
1	649	69 (0)	12 (5)	Rain (6)	11		585
2	663	69 (0)	15 (6)	Rain (7)	13		585
3	670	72 (1)	18 (7)	Rain (8)	16		575
4	647	73 (1)	12 (5)	Heavy clouds (7)	13		570
5	612	74 (1)	15 (6)	Heavy clouds (8)	15	+3	520
6	531	74 (1)	15 (6)	Rain (6)	13	+3	460
7	560	75 (2)	15 (6)	Heavy clouds (8)	16	+6	460
8	595	76 (2)	10 (4)	Overcast (4)	10	+4	520
9	564	67 (−1)	15 (6)	Rain (4)	9	−4	535
10	508	67 (−1)	18 (7)	Heavy rain (4)	10	−6	485
11	454	67 (−1)	18 (7)	Heavy rain (4)	10	−6	435
12	409	67 (−1)	15 (6)	Rain (4)	9	−3	385

* Standard time.

Table XIII. Effect of Cloudy or Rainy Weather on the Night Load

	System Load, Megawatts	Temperature, F	Wind Velocity, MPH	Cloudiness	Combined Weight	Base Load, Megawatts
9:00 p.m.						
10-3-39	655	55 (2)	5 (2)	Overcast (3)	7	610
10- 9-39	656	73 (1)	15 (6)	Fair (1)	8	610
10-12-39	659	64 (−1)	12 (5)	Heavy clouds (4)	8	610
10-19-39	661	60 (0)	20 (8)	Fair (1)	9	610
10-23-39	664	50 (4)	10 (4)	Fair (1)	9	610
10-24-39	661	48 (4)	8 (3)	Fair (1)	8	610
11:00 p.m.						
10- 3-39	542	56 (1)	3 (1)	Fair (1)	3	530
10- 9-39	557	72 (1)	8 (3)	Fair (1)	5	530
10-12-39	569	61 (0)	8 (3)	Heavy clouds (4)	7	530
10-19-39	558	60 (0)	12 (5)	Clear	5	530
10-23-39	568	49 (4)	8 (3)	Clear	7	530
10-24-39	557	47 (5)	3 (1)	Clear	6	530
9:00 p.m.						
5-17-43	914	75 (2)	8 (3)	Clear (0)	5	870
5-18-43	888	77 (3)	3 (1)	Clear (−2)**	2	870
5-19-43*	910	71 (0)	3 (1)	Partly (2)	3	870
5-20-43	878	62 (−1)	3 (1)	Partly (1)	1	870
5-21-43	943	54 (1)	8 (3)	Rain (4)	8	870
11:00 p.m.						
5-17-43	753	70 (0)	5 (2)	Clear (0)	2	740
5-18-43†	784	74 (2)	8 (3)	Clear (−2)**	3	740
5-19-43	776	70 (0)	5 (2)	Overcast (3)	5	740
5-20-43	769	56 (1)	5 (2)	Partly (1)	4	740
5-21-43	797	52 (2)	5 (2)	Rain (4)	8	740

* Stores open; 14-megawatts increase.
† Night ball game; 22-megawatts increase.
** Brilliant full moon.

Fourth hour in advance of normal darkness—add 3.

Normal darkness is considered as occurring approximately 15 minutes after the time the street lights are scheduled. Table XI is an instance of this effect.

On this day, the street lighting was scheduled for 6:50 p.m. The low hanging clouds resulted in a murkiness that gave added weight to the clouded condition in its effect on customer demand for the hours of 4:00 p.m., 5:00 p.m., 6:00 p.m., and 7:00 p.m.

Should the sky be overcast with high clouds, the added weight may be one-third of those as given in the table.

Effect of Inclement Weather on Evening Load

The effect of rainy weather on the evening load that results from the staying home of a great number of persons that in clear weather would be elsewhere tends to build up the load in the earlier part of the evening and tends to reduce the load in the latter part as plans to "make-up for lost sleep" are put into effect. Confirmation of this is to be seen in the example in Table XII. The hour of normal darkness is 8:00 p.m., the street lighting being scheduled for 7:45 p.m.

Table XIV

Wind Velocity, MPH	Weight
25	10
20	8
15	6
10	4
5	2
0	0

Effect of Cloudiness on the Night Load

The effect of cloudiness on the day load has long been apparent in estimating the daily system load. The effect of cloudiness on the early evening peak was considered to be a result of advancing the peak period, thus causing a greater overlapping of the lighting, industrial, and railway load which resulted in a greater peak load. The effect of cloudiness, however, persists throughout the 24 hours of the day.

Table XIII, one of many instances indicating the effect at 9:00 p.m. and 11:00 p.m., supports this statement. This effect on the load of cloudiness or rain is also apparent during the early morning hours.

Ground Snow

A blanket of snow on the ground and on the roofs of buildings increases the brightness and a negative weight must be applied for this factor. It would appear that on a cloudy day or during the night a negative weight of minus two is applicable and on a clear day it is possible the weighting may approximate minus five. The latter weighting implies that the entire territory supplied by the power company is blanketed with a white carpet of snow and is reflecting the rays of the sun.

Cloud Reflection

The effect of the reflection of the sun's rays from banks of clouds in increasing the brightness of the day appears to approximate a negative weight of minus

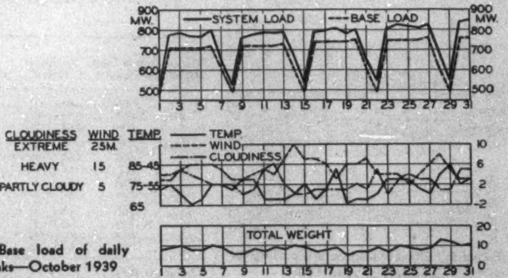


Figure 6. Base load of daily system peaks—October 1939

Table XV

Date	System Load, Megawatts	Temperature, F	Wind Velocity, MPH	Cloudiness	Weight	Base Load, Megawatts
9-11-39	683	70 (0)	10 (4)	Fair (1)	5	650
9-12-39	691	66 (−2)	18 (7)	Fair (1)	6	650
9-18-39	693	59 (0)	15 (6)	Fair (1)	7	650
9-19-39	707	62 (−1)	20 (8)	Partly cloudy (2)	9	650
9-20-39	674	69 (0)	5 (2)	Partly cloudy (2)	4	650
10-14-39*	672	60 (0)	25 (10)	Clear (0)	10	610
5- 4-43	1,023	54 (1)**	15 (6)	Partly cloudy (2)	9	940
5- 6-43	976	70 (0)	10 (4)	Clear (0)	4	940
5- 7-43	977	56 (1)**	10 (4)	Clear (−1)	4	940
5- 1-43	941	50 (2)**	15 (6)	Clear (0)	8	870

* Saturday load reflecting appreciable increase in railroad demand.

** Reflects temperature ceiling of 65 degrees of fuel rationing.

two, depending on the extent of the scattered or broken clouds.

Wind

Wind a Factor in the Demand

Wind, in its effect on combined customer demand, apparently should be given a weight of two for each five miles per hour of its velocity. The increased demand of railway transportation as a result of added resistance to the movement of trains and cars and the greater radiation losses of heating devices are some of the causes of load increase due to higher wind velocities. The weight scale in use is given in Table XIV.

The effect of the wind on the load is indicated in the weight of the factors and the determination of the base load as given in Table XV.

Combined Weight of Weather

Figure 2 indicates the weighting of the weather for the morning and evening peak periods of midweek days of the week ended February 7, 1942.

Daily Peak and Hourly Base Loads

The feasibility of this method of estimating load must be in the consistency of the calculation of the base load. Such consistency must be evidenced in the following manner:

1. The base load of the daily peak period must be uniform for the midweek days.
2. The base load for each hour of these days must be uniform, excluding Monday 12:00 a.m. to 8:00 a.m.
3. The peak and hourly base loads of each Saturday over a period of a number of weeks must be consistent.
4. The peak and hourly base loads of each Sunday over a period of a number of weeks must be consistent.

It was this uniformity in the base load that furnished convincing support to the idea, and to indicate this uniformity the following figures and tables are submitted:

Figure 1—Graph of sum of the weights of each factor applied to daily peak, August 1939, to determine peak base load.

Table XVI. Hourly Base Loads

Saturdays in October 1939

Time	Megawatts			
	Oct. 7	Oct. 14	Oct. 21	Oct. 28
1 a.m.	390	390	390	390
2 a.m.	335	335	335	335
3 a.m.	320	320	335*	320
4 a.m.	305	305	305	305
5 a.m.	315	315	315	315
6 a.m.	335	335	335	335
7 a.m.	365	365	365	365
8 a.m.	450	450	450	470**
9 a.m.	500	500	500	520**
10 a.m.	510	510	510	530**
11 a.m.	500	500	500	520**
12 a.m.	500	500	500	500
1 p.m.	475	475	475	475
2 p.m.	460	460	460	460
3 p.m.	450	450	450	470**
4 p.m.	450	450	450	470**
5 p.m.	460	460	485**	485**
6 p.m.	560	560	575**	600**
7 p.m.	580	580	590**	600**
8 p.m.	550	550	550	570**
9 p.m.	520	520	490***	520
10 p.m.	500	500	470***	500
11 p.m.	460	460	435***	460
12 p.m.	410	410	400***	410
6:15 p.m. (peak)	600	610	620**	630**

* Railroad heavy.

** Last Saturdays of World's Fair.

*** Electrical storm; silencing of radios and subsequent early retiring.

Figure 6—Graph of the same for October 1939.

Figure 7—Graph of the same for December 1939.

Table XVI—Hourly base load for a number of Saturdays.

Table XVII—Hourly base load for a number of Sundays.

Table XVIII—Hourly base averages for the midweek days over a number of weeks.

Table XIX—Comparison of weekly system peaks and base loads, 1938 and 1939, June to December, inclusive.

Determination of Peak Base Loads

Table XIX is a comparison of the daily base loads of each week of the corresponding months of 1938 and 1939, beginning with June. In this record, it is evident that there will be little change in the daily peak base load from the first of June until the end of the daylight-saving period, unless there is an uptrend in business as is indicated in September of 1939.

Table XVII. Hourly Base Loads

Sundays in October 1939

Time	Megawatts			
	Oct. 1	Oct. 8	Oct. 15	Oct. 22
1 a.m.	350	350	350	350
2 a.m.	290	290	290	290
3 a.m.	275	275	275	275
4 a.m.	270	270	270	270
5 a.m.	260	260	260	260
6 a.m.	260	260	260	260
7 a.m.	260	260	260	260
8 a.m.	290	290	290	290
9 a.m.	340	340	340	340
10 a.m.	350	350	350	350
11 a.m.	350	350	350	350
12 a.m.	350	350	350	350
1 p.m.	350	350	350	350
2 p.m.	340	340	340	340
3 p.m.	340	340	340	340
4 p.m.	350	350	350	350
5 p.m.	365	365	365	365
6 p.m.	440	460	470	470
7 p.m.	485	495	495	495
8 p.m.	485	490	490	490
9 p.m.	485	485	485	485
10 p.m.	450	450	450	450
11 p.m.	420	420	420	420
12 p.m.	360	360	360	360
Morning peak, 11:30 a.m.	355	355	355	355
Afternoon peak	355	355	355	355
Evening peak	500	500	500	500

Table XVIII. Hourly Base Loads

Midweek Days (Not Including Monday 12 to 8 A.M.)

Time	Megawatts			
	Oct. 7*	Oct. 14*	Oct. 21*	Oct. 28*
1 a.m.	370	370	370	370
2 a.m.	325	325	325	325
3 a.m.	310	310	310	310
4 a.m.	300	300	300	300
5 a.m.	310	310	310	310
6 a.m.	345	345	345	345
7 a.m.	410	410	410	410
8 a.m.	580	580	580	580
9 a.m.	640	640	640	640
10 a.m.	630	630	630	630
11 a.m.	620	620	620	620
12 a.m.	610	610	610	610
1 p.m.	600	600	600	600
2 p.m.	610	610	610	610
3 p.m.	610	610	610	610
4 p.m.	620	620	620	620
5 p.m.	640	640	640	640
6 p.m.	710	720	730	730
7 p.m.	670	670	670	670
8 p.m.	640	640	640	640
9 p.m.	610	610	610	610
10 p.m.	580	580	580	580
11 p.m.	530	530	530	530
12 p.m.	460	460	460	460
Morning peak, 8:30 a.m.	650	650	650	650
Afternoon peak, 3:00 p.m.	610	610	610	610
Evening peak	710	720	740	760

* Week ended.

When the system peaks occur in the evening as a result of the earlier darkness; there may be a change in this base load each week because of the advance of the period of darkness. Thus it is desirable to increase the evening peak base load somewhat for the Monday estimate and

Table XIX. Comparison of Weekly System Peaks and Base Loads

1938 and 1939, June to December Inclusive

Week Ended	1938 System Peak	1938 Base Load	1939 System Peak	1939 Base Load
June 4	624	570	673	615
11	619	575	673	620
18	610	575	676	620
25	634	575	681	620
July 2	616	575	682	620
9	591	580	658	620
16	620	580	675	620
23	620	580	665	620
30	617	580	678	620
Aug. 6	654	580	675	620
13	628	580	690	620
20	648	580	682	620
27	635	580	700	620
Sept. 3	626	580	684	620
10	609	580	699	640
17	653	580	691	650
24	678	580	707	650
Oct. 1	706*	600	778*	680
8	726	625	794	710
15	712	635	790	720
22	736	650	807	740
29	773	660	827	750
Nov. 5	771	715	848	790
12	767	715	838	800
19	788	720	875	805
26	837	720	896	805
Dec. 3	804	750	864	835
10	807	750	889	835
17	827	760	886	835
24	830	765	898	860
31	823	750	907	820

* Evening peak period begins.

Table XX. Effect of Radiobroadcasts on System Loads

Time, P.M.	Normal Base, Megawatts	Abnormal Base, Megawatts	Increase, Megawatts
Night baseball; June 26, 1939, Yankees and Athletics, game started 8:00 p.m.			
5	520	520	
6	460	460	
7	460	465	+ 5
8	515	537	+22
9	535	563	+28
10	485	493	+ 8
11	435	435	
12	385	385	
Night baseball; September 11, 1939, Red Sox and Athletics, game started 7:00 p.m.			
5	545	545	
6	495	495	
7	600	629	+29
8	575	600	+25
9	545	571	+26
10	495	495	
11	445	445	
12	390	390	
Heavyweight fight; September 15, 1939, Galento and Nova, 9:00 p.m.			
7	600	600	
8	575	575	
9	545	578	+33
10	495	521	+26
11	445	453	+ 8
12	390	398	+ 8
President's speech to Congress; September 21, 1939, 3:00 p.m.			
1	600	600	
2	600	600	
3	595	613	+18
4	595	605	+10
5	555	555	

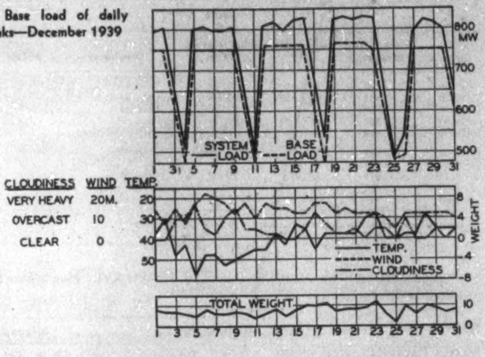


Figure 7. Base load of daily system peaks—December 1939

on Tuesday determine what corrections must be made. As the period of darkness recedes in February and March, the evening peak base load should be decreased in a like manner. As the months change, as stated previously, corrections of the base load will have to be made when the temperature base is changed.

Increase of Load From Radio Broadcasts

It is evident that if the hourly base load is known and the weight of the factors of the degree of cloudiness, wind velocity, and temperature is known, the determination of the amount of the load increase due to radio broadcasts of great interest will be facilitated.

As an instance of this, the increase of load due to the broadcast of a night ball game, of a prize fight, and of the President's speech to Congress is indicated in Table XX. The increase in load from the broadcast of night baseball appears to be a direct reflection of the attendance at the ball park. If the attendance is 20,000 the increase in load will approximate 20,000 kw. As noted in the table, when the attendance was approximately 30,000 the increase in load approximated 30,000 kw.

Electrical Storms

There is an appreciable potential decrease of load on the approach of and during the progress of an electrical storm, principally due to silencing of radios. This effect, however, is counteracted by the increase due to the greater degree of cloudiness and the stronger winds. On October 21, 1939, the potential decrease during an evening storm approximated nine per cent, as indicated in Table XXI.

The approximate potential effect of an

Table XXI. Load on October 21, 1939 (Saturday)

Time, P.M.	System Load, Megawatts	Temperature, F	Wind Velocity, MPH	Cloudiness	Weight	Base, Megawatts	Normal Base, Megawatts
7	612	67 (−1)	5 (2)	Partly cloudy (2)	3	590	590
8	594	66 (−2)	15 (6)	Partly cloudy (4)	8	550	550
9	533	57 (1)	12 (5)	Light rain (5)	11	480	520
10	519	60	12 (5)	Heavy rain (6)	11	470	500
11	485	60	15 (6)	Light rain (5)	11	435	460
12	440	59 (1)	10 (4)	Light rain (5)	10	400	410

Table XXII

Time, P.M.*	System Load, Megawatts	Temperature, F	Wind Velocity, MPH	Cloudiness	Weight	Base, Megawatts	Normal Base, Megawatts
August 3, 1939							
1	648	89 (6)	15 (6)	Overcast (5)	17	555	585
2	635	75	15 (6)	Heavy clouds (7)	13	560	585
3	644	77 (1)	15 (6)	Heavy clouds (6)	13	570	575
4	637	78 (1)	15 (6)	Overcast (4)	11	575	575
August 16, 1939							
2	635	86 (5)	5 (2)	Partly cloudy (2)	9	585	585
3	633	86 (5)	12 (5)	Heavy clouds (6)	16	545	575
4	614	83 (3)	10 (4)	Partly cloudy (3)	10	558	575
5	578	82 (3)	2 (1)	Partly cloudy (3)	7	540	540

* Standard time.

Table XXIII. Daily Schedule Data

Tuesday, June 1, 1943

Station	Megawatt-Hours		Assigned Load and Capacity, Megawatts								
			Early A.M.			Day Period			P.M. Peak		
	Estimated	Actual	Avg	Max	Units	Avg	Max	Units	Avg	Max	Units
Barbadoes	200	163			0	10	25	1			0
Chester	4,100	3,984	140	175	3	240	255	6	195	240	5
Deepwater	1,400	1,394	20	95	4	75	90	4	65	80	4
Delaware	1,800	1,827	5	30	1	132	180	6	88	120	4
Richmond	3,900	3,975	60	135	1	180	192	2	180	192	2
Schuylkill	2,600	2,693	115	133	4	115	133	4	115	133	4
Newlinville	335	336	13	14		18	19		17	18	
Flat Rock	35	35	1	1	4	1	1	4	1	1	4
Conowingo	6,250	6,269	Max	260	7	Max	260	7	Max	260	7
Pennsylvania Water Power interconnection	+200	+193				+10	10		+10	10	
Pennsylvania-New Jersey interconnection	−1,620	−1,297									
Generated	20,820	20,869									
System	19,200	19,572									

Load Estimates:	12–6 a.m.		6–12 a.m.		12–7 p.m.	7–12 p.m.	
Capacity, megawatts	843	1,133	1,133	1,165	1,169	1,054	1,054
Load, megawatts	470	996	1,015	1,034	984	922	905
Reserve, megawatts	373	137	118	131	185	132	149
Base load, megawatts			940		920	870	
Weather Weight:							
Temperature		1	1	0	1	1	1
Wind		2	3	2	2	3	3
Cloudiness		3	4	8	4	2	0
Total weight		6	8	10	7	6	4

Weather:
Predicted—Cloudy

Actual—Very cloudy

Temperature—Maximum 75 deg

Conowingo:

Estimated flow—80,000 cfs

Actual flow—71,075 cfs

Elevation—spilling

Deepwater total output:

2,901 megawatt-hours

Established peak transfer

9 a.m.—4
6 p.m.—4

electrical storm on the load in the daytime is indicated in Table XXII.

The potential reduction in load due to electrical storms in the daytime approximates 30 megawatts.

Use of Method

This method has been followed in estimating the load for the past five years. In 1938, there was a prolonged period of "cut and try" to arrive at a proper weighting of the weather components. In addition, during this period, the daily loads of the previous years and the weather as given in the summaries of the Weather Bureau were analyzed. It was apparent almost immediately that there was a base load to which the weather weights could be applied as a percentage to arrive at an estimate of the system load.

Revision of Load Estimate

At noon, the weather forecast is received and an estimate of the system load is made, generating capacity is scheduled, and the station superintendents, coal bureau, and interconnection office are notified. The schedule card is as shown on Table XXIII.

The morning peak of a clear day occurs at 8:30 a.m. During the summer months of high temperatures, the morning peak may occur as late as 11:00 a.m., if the temperature increase is appreciable. At 6:30 a.m., the load dispatcher applies the proper weight to the weather as predicted at 6:00 a.m. by the Weather Bureau and as indicated by existing weather conditions, and the load estimate is revised by him if necessary. If the weather is very cloudy, the morning peak will occur between 9:30 a.m. and 10:30 a.m., permitting a revision of the estimate at 8:30 a.m. if the required additional generating capacity may be made available for load.

In the copy of the schedule card, the initial estimate of the morning peak was 996 megawatts, based on a predicted temperature of 73 degrees, a wind velocity of five miles per hour, and high clouds and overcast sky. At 6:30 a.m., the estimate was corrected for a predicted temperature of 73 degrees, a wind velocity of eight miles per hour, and an overcast sky with three-mile visibility. As shown on the schedule card, the weather weight was increased from six to eight and the load estimate was raised to (940×1.08) = 1,015 megawatts. At 8:10 a.m., a revision was made in the load estimate because of the continued lowering of the clouds and for a temperature of 70 degrees and a wind velocity of five miles per hour. The combined weight of ten called for a revised estimate of (940×1.10) = 1,034 megawatts. The actual peak of 1,043 megawatts occurred at 9:30 a.m.

Responsibility of Load Dispatcher

Revisions of the daily load estimates are a daily occurrence, and it is desirable that this responsibility be assigned to the load dispatcher on duty as he is in the best position to observe the trend of the load and the effect of the weather on the load.

American Telegraphy After 100 Years

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Synopsis: The telegraph, which is the oldest electrical industry, celebrated its centennial on May 24, 1944. Technically, its operations have changed greatly during recent years, but the only data which have been published regarding such changes pertain to specific applications or developments. The present paper is intended to present an over-all picture of American telegraphy as it is conducted today by the one company which is engaged solely in the domestic telegraph business.

The paper explains the nature of telegraph service, discusses the traffic problems associated with the business, describes the facilities which are employed, and the functions which they perform. Although the paper is long by reason of the multiplicity of telegraph services and types of apparatus, only brief references are made to features of plant or equipment which have become stabilized through the years and well understood by the industry. Similarly, no attempt is made to discuss details of technical design or operation, nor the various historical steps by which the telegraph reached its present status. In brief, the paper presents merely a general view of the industry at the end of its first century of existence.

MODERN man is so beset by experiences and so accustomed to the unusual and the new that he seldom pauses long enough to consider where these things come from or why they happen. He does not realize that he is having science forced upon him all the time, that almost his every act and his every thought have been directed or influenced in some way by that intangible something that is called electricity. It was only ten years ago that this great society celebrated its golden anniversary. It lauded the accomplishments of its scientists and engineers; it recited the long list of modern comforts and great industries that owed their very being to electrical discoveries. So it is with the greatest of pride that the writers of this paper remind their readers that all of these great industries which harness electricity for the benefit of mankind had their beginning just 100 years ago when Samuel Finley Breese Morse introduced to the world his working electromagnetic telegraph.

There had been great inventions before, such as the steam engine, the cotton gin, and the sewing machine; and scientists had experimented with the electric-current deflecting needles, repelling pith balls and the like. The Leyden jar and the electric battery also had been invented, but no prior application of electricity ever contributed to the welfare of man as did the telegraph. It was the real beginning of electrical engineering.

It is not necessary to dwell on the trials and tribulations of the inventor nor on his biography. This year is the centennial of the telegraph, and appropriate ceremonies already have been held, both at Franklin Institute in Philadelphia, Pa., where the first experimental model of the telegraph was shown in 1838, and in Washington, D. C., where in 1844 the public demonstration of "What Hath God Wrought" took place. The present paper is intended to describe the telegraph as it is, 100 years later.

By reason of limitations as to time and space, this discussion will be confined to the business as conducted by The Western Union Telegraph Company, which now, since its absorption of the Postal Telegraph Company, is the only American company engaged solely in the domestic telegraph business. Items of plant and equipment which have been in use throughout the years, with little or no change in fundamental character, will be mentioned only casually or not at all. No attempt will be made to discuss the technical complexities of design or operation. In brief, it is proposed to present only an over-all picture of present-day telegraph technology.

Telegraphy is not simply a system for transmitting information across vast distances—it is a communication service of "record." Like the telephone, it traverses long distances, but its function is to provide at the destination a written or printed record of the information which was transmitted. This record, accurate and permanent, is the indispensable tool of business. The business man, the engineer, the professional man, and the worker cannot carry on without the quick transmission of information between distant points and the recording of that information, so that there will be a minimum chance of misinterpretation or mistake.

In its simplest form telegraph service has a great similarity to mail service, in that it starts at the point of origin with a piece of paper bearing a message, and it delivers at the destination a piece of paper bearing the same message. Both services essentially involve personal contact at both ends between the public and the carrier, the element of time being the prime characteristic difference between them. That is why the air-mail service, through its lessening of time for mail transportation, now handles so many communications of medium urgency that were formerly handled by telegraph. As air-mail service expands and speeds up, the telegraph must necessarily become more and more a service to fulfill the need of highest urgency. The trend of future development will be to minimize the time element.

Telegraph Services

Before discussing the technical systems and devices used, it seems wise first to outline the nature and peculiar characteristics of telegraph communication services. First there is the telegraph service rendered to the public in general. A piece of paper bearing the message is physically carried from or handed by the sender to the nearest branch telegraph office. It is then wired to the main office, being there recorded on a second piece of paper which is moved around the room to the appropriate outgoing trunk position. Upon transmission to the city of destination it is recorded on a third piece of paper, moved through the room to the city line position of the appropriate branch office, and transmitted to that branch where it is recorded as a fourth piece of paper which is physically delivered to the addressee. If pickup or delivery is from the main office, or if the branch office has pneumatic-tube connection to the main office, then one less piece of paper is involved. On the other hand, if there are no direct trunks between the cities, there will be a relay at an intermediate point, involving an additional piece of paper. Obviously, the physical handling of pieces of paper is an important factor in rendering service to this class of customers; it probably has more influence on the speed of service than any other factor.

Then comes the service to business houses or other users, who have sufficient need for the telegraph to justify the expense for special equipment, by means of which the messages may be conveyed to and from the telegraph office electrically, rather than by the physical handling of a piece of paper. In these cases, the patrons operate their own sending and receiving equipment.

The next type of service involves the leased circuit between customers' offices by means of which the customers themselves perform all sending and receiving operations; the telegraph company merely provides the circuit and supervises its performance. Such telegraph connections may be set up either for definitely assigned periods of time or, on demand, for short usage, just as telephone connections are established.

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A special class of this service is that in which customers who have large networks of leased private circuits are provided with special switching facilities at central points, by means of which the user himself can set up connections for switching traffic between any two or more points reached by his private wire network.

A further development is an expansion of the two last-mentioned services by which many stations are connected together for the simultaneous transmission of information to the other stations, either from one main sending station or from any one to the others. This is a limited form of broadcasting. Such networks are employed by newspapers and press associations, by governmental departments, by brokerage houses, and by other industrial organizations which have numerous branches or plants in widely separated localities.

Special applications of the last-mentioned type of service are the quotation service rendered for stock markets and produce exchanges, and the news and sports services that reach all parts of the country. The most common symbols of these services are the news and stock tickers often seen in business houses, clubs, and the offices of security brokers. Another closely related service is that of furnishing and operating "automatic stock-quotation boards"—display boards which, by telegraphic control, automatically post price changes of market securities immediately after transactions take place. A third auxiliary service is that of furnishing correct time through the means of time pieces which are periodically synchronized with master clocks controlled by signal from the naval observatory at Washington, D. C.

In addition to these various land-line telegraph services, international telegraph communication by cable and radio must be included as one of the very important services in which the land-line telegraph company participates. In the case of cables operated by the Western Union Company, the telegraph company has entire charge of all business, plant, and operations. In the case of cables and radio circuits owned and operated by companies which have terminal facilities comprising only the international operating stations, the handling of terminating business to and from the land-line patrons is conducted by the telegraph company, except in those cities where the international operating stations are located. Except for the greater use of codes in order to attain brevity, international communications so far as the public is concerned are not greatly different from land-line telegraphy.

While the foregoing lists practically all of the fundamental types of record communications, there have grown up through the years many modifications of these services as well as many auxiliary services, most of which were created either to satisfy a public need or to attain better utilization of plant or personnel otherwise required for purely telegraph purposes. Among the first group may be mentioned day letters and night letters, money transfer by wire, special holiday and greeting messages, and other fixed text telegrams. In the second group are such activities as messenger errand service, parcel deliveries, shopping services, ticket reservations, and so forth. Although activities of this character, before they were curtailed on account of the war, were of no little importance in the day-to-day operations of a telegraph company, they do not involve technical facilities essentially different from those employed for other services; hence they will not be considered further in this paper.

Traffic Considerations

The function of telegraphy is primarily to provide record communication service between points which are so far apart that physical delivery of a message will not be sufficiently expeditious. This condition limits the use of the telegraph for local communications and hence has caused the telegraph system to become a network of trunk lines connecting traffic centers, scattered far and wide throughout the country and serving as collecting and distributing centers for all telegraph business. These centers are always cities or towns which directly serve populated areas of considerable extent. The local public in the cities is served by main and branch offices and by direct local circuits to the larger users. Surrounding towns and communities are served by tributary circuits from the central city. Each community in turn has one or more local offices and as many circuits to local patrons as the needs require. The system of the Western Union Company reaches 18,866 telegraph offices and over 14,000 agencies.

Circuit Layout

If one could start out today and build an entirely new telegraph plant, the first step in developing the plans would be to estimate the amount of business that could be expected to be handled between all points to be served, one to the other. From these data he would determine between what points there would be enough traffic to justify "terminating" circuits, and what points would be required as junction points for traffic that could not go by "direct" connections. Then he would decide upon the number and character of circuits required between all these various terminals; from this decision he would be able to locate his terminal and junction offices and decide upon the equipment required for each of them.

This is just the kind of work that is continually being carried on in the traffic organization. From studies made of message loads between all points there has been set up a precise schedule which determines the circuit requirements between all offices, what offices shall have direct circuits to what other offices, and how every message shall be routed from origin to destination. There is always a preferred route and, in general, one or more alternate routes for use when the preferred one is not available. These studies determine when existing circuits shall be discontinued and prescribe where new ones shall be established, even to the extent of naming the wires to be used by their individual wire numbers. They specify the method of operation and the location and types of repeaters or regenerators. All of this circuit and routing information is embodied in printed instructions issued by the headquarters office and distributed throughout the country, each office receiving whatever instructions apply to its own activities.

Offices which serve as transfer points for traffic are known in telegraph parlance as "relay" offices, and the operations of receiving and retransmitting telegrams are known as a "relay" of traffic. This use of the word "relay" in connection with telegraph-message handling is peculiar to the business and must not be confused with the same word when employed to designate electromagnetic devices for repeating electrical impulses.

Traffic Routing

The handling of telegrams in a telegraph office is to a certain extent similar to the sorting and distributing of mail in a post office. All of the telegrams originating in a city must get to the main office in that city either directly by messenger, telephone, or customer's tie line, or through a branch office by either wire or pneumatic tube. In usual main-office practice all of these messages flow through a distributing center where each is dispatched either to the appropriate outgoing trunk position or to a subcenter from which it is carried to that position. These operations are performed manually in the smaller offices, but in larger offices belt conveyors and sometimes routing aides on roller skates are used to transfer the messages around the operating room.

Business incoming to the main office from other cities may be either terminating or for relay. In the latter case the messages are handled through the distributing center just mentioned. Terminating business goes to a city routing center where clerks determine the further handling, which may be by customer tie line, telephone, messenger, or through some branch office, which in turn may be reached by tube or wire. The city clerks dispatch this business to that part of the operating room where the facilities involved are located.

Obviously, it is impossible for a clerk to remember all of the information required, so route charts are provided for the outgoing trunk business in the distributing center and for the terminating city business in the city routing center. These charts give all of the information necessary for the handling of business in those centers.

In certain of the newer type reperforator switching offices, which will be described subsequently, incoming telegrams are switched directly to their outlet without copying and retransmitting. In such installations each switching clerk acts also as a routing clerk and hence is provided with complete route charts mounted directly over the receiving equipment.

Traffic Dispatching

No matter how perfectly the routing of telegrams to destination may be worked out and standardized, there are always situations in a nationwide system which require departure from the authorized routine. Lines may be disrupted by storms, floods, or train wrecks; circuits may be inoperative because of aurora borealis, inductive interference, or earth potentials. Central offices may be out of commission on account of fire, floods, or power failure. Sudden surges of traffic may be occasioned by events of unusual character, such as catastrophies, sports, elections, court trials, or governmental activities. Since local supervisory staffs are in general not in a position to meet these emergencies, there is maintained at headquarters a dispatching bureau which receives hourly reports from all main traffic centers throughout the country, advising of unusual conditions or circumstances that may create congestion or an emergency. This bureau then sets up counter measures, diverting traffic around critical areas, arranging for additional circuits to points of heavy load, and taking whatever other steps may be necessary to meet the situation.

Trunk-Line Plant

The trunk-line plant of the Western Union Company is a network of open-wire pole lines, with cables at entrances into cities and in limited trunk sections where the number of circuits is too great to permit safe construction with open wires. The cables are carried aerially where conditions permit. Elsewhere they run in underground conduits, or as submarines where crossing rivers or other inland bodies of water is necessary.

Telegraph lines are predominantly along railroad right of ways; only a very small mileage follows highways or cross-country routes. This close relationship between the telegraph and the railroad dates back to the pioneering days when railroads first stretched their lines to the new outposts of American industry. The railroads needed the telegraph to operate their trains and carry on their business; the telegraph companies, which then were small in size and large in number, needed right of ways, transportation for men and materials, and offices and operators at the way stations along the routes. The arrangements were to the mutual advantage of both industries. Today, with only one telegraph company, there is practically no railroad in the country which does not have the Western Union Company contracts covering pole lines and wires on its right of ways.

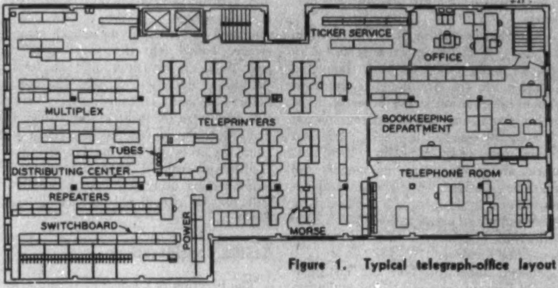

Figure 1. Typical telegraph-office layout

The telegraph plant within the United States, and including extensions to cable stations in the maritime provinces, aggregates 238,700 miles of pole line involving approximately 7,000,000 poles. Open-wire mileage exceeds 2,300,000 miles. About 72 per cent of this is copper, mostly number 9 American wire gauge. The remainder is iron or steel with number 8 Birmingham wire gauge the predominant size. Larger sizes have been employed in special locations where unusually heavy storm conditions are encountered, but the preferred method of securing greater line-wire strength is to use high-strength bronze or copper having a core of high-strength steel.

The major portion of the telegraph-wire plant is carried on ten-foot ten-pin wood crossarms; there are seldom more than five or six arms to the pole. Pins are of steel with wood cobs, although during recent years much new construction has been made with wood pins. The standard insulation is glass of a double petticoat design. A recent development, introduced to the telegraph plant about five years ago, is an insulator made of a rubber compound designed to take the place of glass in specific localities where malicious breakage of insulators from stones, rifle bullets, and so forth is so prevalent as to require their frequent replacement.

Cable construction in the telegraph plant follows recognized practices in the communications industry. Cables are paper insulated and lead sheathed. For trunk purposes conductors are of number 16 American wire gauge or larger, with insulation designed for 1,200-volt breakdown test. Wires are paired and also quite generally quadded. For local distribution purposes in cities and towns cables are usually of number 19 American wire gauge insulated for 500-volt breakdown test.

While the basic character of the trunk-line-wire plant has not changed appreciably through the years, the development and use of high-efficiency operating systems has imposed many technical refinements on the plant construction and particularly on its operating characteristics. As more and more message capacity is derived from each wire, it becomes increasingly important to secure the maximum continuity of operation. In present-day telegraphy, therefore, greater precautions are required than formerly were necessary to guard against line interruptions due to pole failures from decay, overloading or accident, circuit failures caused by wire breakage, wire crosses or electrical hazards, and cable failures due to damage from accident or corrosion.

High-speed operation also imposes severe limitations on the amount of extraneously caused currents that can be permitted in telegraph wires. Mitigation of inductive interference from neighboring electric-power lines, therefore, has become a major problem, and close co-operation is maintained with electric-light and power organizations in order to avoid interference where possible and to lessen its effects when complete avoidance is not practicable. Not only have power companies made many changes in their plans and systems in order to minimize interference effects, but also elaborate neutralizing systems have been installed in the telegraph plant. In some of these, pilot wires are carried on the telegraph lines throughout sections which are exposed to inductive effects, and electronic devices are installed at the terminals or junction points to generate neutralizing potentials which are introduced into the affected circuits.

Crossfire interference between the various telegraph wires themselves is another factor that influences high-speed operation. On important trunk lines this disturbance has been minimized effectively through the installation of neutralizing networks at the central-office terminals. Earth potentials are another cause of disturbance, and in a number of situations it has been necessary to run special ground wires to distant points of more constant potential or to install automatic neutralizing systems controlled by pilot wires to points out of the zone of disturbance.

The gradual introduction of high-frequency carrier telegraph systems employing metallic circuits is another factor that has altered trunk-line practices in the telegraph industry. In order to provide the high degree of circuit balance necessary for such operation, pole lines have been rearranged to secure close pairing of like wires, sources of resistance or leakage unbalance in the circuits have been eliminated, and transposition systems and methods capable of preventing high-frequency cross induction have been developed. It also has been necessary to standardize coils for inductive loading of aerial and underground cable circuits and to provide for matching the circuit-impedance characteristics at junction points between open wire and cable.

Central Offices

The telegraph central office is the pivot about which revolve all activities of the telegraph business. It is the point from which radiate the trunk circuits to all distant cities and traffic centers; it is the focus of all telegraphic communication from the nearby populated area. In it takes place all the crisscross of communications, the movement of untold numbers of papers bearing important messages, and the interconnection of electric circuits carrying these messages in all directions.

Office Buildings

In many of the larger cities The Western Union Telegraph Company has established its own central-office buildings, designed and built to provide the special features needed for the installation and operation of telegraph equipment. The largest of these, in New York, houses the general offices of the company and has a usable floor area of 731,000 square feet.

Where special telegraph buildings have not been provided, the telegraph central offices are located in regular office buildings or, as in small towns, in store buildings. In such situations it usually has been necessary to install special conduits or ducts to bring the power and communication wires into the building and in some cases to provide special reinforcements to take care of the increased floor loadings.

If a large office is used as a basis for discussion, telegraph central-office equipment may be classified roughly into the following categories:

1. Wire terminating equipment, including entrance cables, protectors, distributing frames, and switchboards.

2. Intermediate operating equipment, including repeaters, concentrators, and operating-system apparatus, such as multiplex, Varioplex, carrier, and Telefax.

3. Operating positions, including operating tables with Morse, multiplex, and teleprinter receiving and sending apparatus, tape perforators, transmitters, telephone terminals, Telefax terminals, switching facilities, and so forth.

4. Message-handling equipment, including belt conveyors for message handling between incoming and outgoing operating positions, distributing centers, and terminals of branch-office pneumatic tubes.

5. Power equipment, including power switchboards, rectifiers, and motor generator equipment for converting commercial electric power to the type and voltage needed for telegraph operations; power distribution systems to various telegraph-equipment installations; and often storage batteries or engine-driven generator units to supply emergency power in the event of failure of commercial supply.

6. Building equipment, such as adequate illumination for all purposes, elevators, heating supply, ventilating and plumbing equipment, clothes lockers and rest rooms, and restaurant facilities, where required.

Office Layout

The layout of a central office has to accommodate itself to the structural characteristics and floor plan of the building. Although its size and importance vary greatly, the planning of an office is chiefly directed to two basic requirements: first, to provide a minimum travel time, implying a minimum travel distance, for the telegrams as they move through the office in the processes of distributing and relaying; and second, to place the various items of equipment so as to require a minimum of wiring and cabling between them.

In the operating department the distributing center forms a focus for all traffic movement. Operating tables are arranged in rows to facilitate supervision. Trunk positions are usually separate from local positions, with Morse, multiplex, teleprinter, and Telefax installations each grouped by themselves. Figure 1 shows a layout that is representative of a medium-sized office. Where the operating department occupies more than one floor, as is the case in New York, Chicago, St. Louis, Philadelphia, Boston, Washington, and Los Angeles, the same principles guide, but the layout is made in three dimensions rather than two.

As a separate unit but as close as practicable to the operating area are the distributing frames, switchboards, repeaters, multiplex and carrier equipment, and all other apparatus which is regularly supervised by what is known as testing and regulating forces. Power conversion equipment is usually nearby, although emergency engine generator units are customarily placed in basements.

Telegraph Power

Wherever possible, telegraph power is supplied by commercial electric-power sources, either direct or alternating current as the case may be. Since most

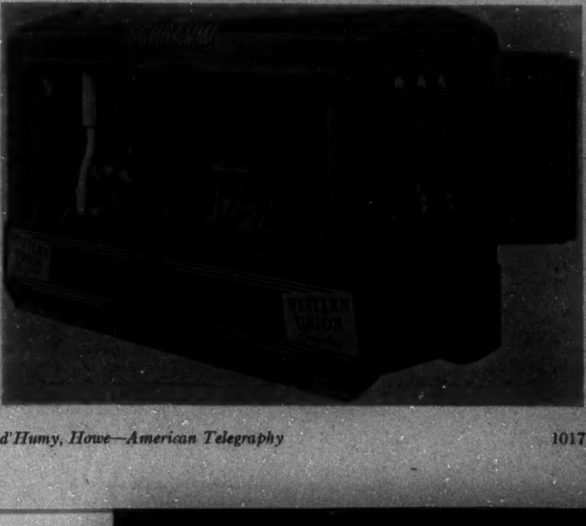

Figure 2. Portable emergency power unit

telegraph operations are still conducted on a d-c ground-return basis, both positive and negative polarities are required. The standard line potential is 160 volts, the 240-volt supply which used to be quite common now largely having been eliminated. Most local circuits are 120 volts direct current, either positive or negative, although a few applications require 24 volts. During recent years the expansion of Varioplex and the growth of Telefax have greatly increased the use of electric motors, many of which must maintain operation at constant speed. Consequently, the simplicity and outstanding advantages of synchronous a-c motors have caused a great growth in the use of alternating current, usually of the 120-volt 60-cycle standard commercial supply.

By reason of the multiplicity of electric power supplies needed for telegraph operations, considerable conversion equipment is necessary to provide the proper type of supply and voltage. Where the commercial supply is only direct current, motor generator sets include booster generators to provide the increase in direct voltage needed for telegraph operations and alternators to provide the necessary alternating current. Where a-c commercial supply is available, motor generator sets are used to obtain the d-c potentials, if the office is large. In small offices, however, it is customary to employ mercury-vapor rectifiers to produce the d-c potentials, the commercial a-c supply being used without change for the a-c potentials.

Since continuity of service is one of the prime essentials of a telegraph office, every effort is made to insure dependability of power supply. Where possible, separate and independent power supplies are brought into an office, preferably by routes and entrances so located that an accident to one supply will not affect the other. When such fallback power services are not available, and the importance of the office justifies it, separate engine-driven emergency generator units are provided. These are usually gasoline operated, although Diesels are used in some installations, and in a few cases natural or manufactured gas is the source of power.

Because it has not been practicable to install emergency power units in all central offices, there also have been provided a number of portable emergency power plants, strategically located throughout the country, ready for quick movement into localities where disaster may have interrupted the regular power supplies. These units, of which Figure 2 is typical, have a capacity of 25 kw which is sufficient to take care of emergencies in most of the offices which do not have emergency plants of their own. The unit illustrated is interesting in that the engine is that of an ordinary Ford truck, and the generator is one of special design which will deliver its full capacity as either direct or alternating current, or part direct and part alternating current.

Message-Conveyor Systems

As described under "Traffic Routing," messages must be moved physically about the operating rooms. In small offices the distances are not great and the messages are simply transferred manually. In large offices, however, the operation is complicated, both by the distances and the large number of messages handled. For example, it is not uncommon for the New York or Chicago office to have as many as 250,000 to 350,000 separate message handlings daily. This intraoffice distribution of telegrams in large offices is accomplished by means of belt conveyors. The present-day types consist of V belts (really a narrow belt just below an open bottom V-shaped trough) running along the backs of the operating tables, so that telegrams may be dropped upon them at almost any point. These belts carry messages to the main or city distributing centers where they are distributed to subcenters located in various parts of the operating rooms, close to the operating positions which they serve. Flat belts, 11 inches wide, are still in use in some of the older offices. Where necessary to run overhead or between floors, strap or drag belts or "twist" belts are used. These are operated much faster than the table belts and can run for considerable distances without intermediate supports.

Belts operate at speeds of from 60 to 700 feet per minute, depending on the type of belt and character of service. In the New York office the belt distributing system employs no less than five miles of belting and includes four distributing centers, each located on a separate floor of the operating department. Figure 3 shows the V belts immediately behind a row of operating tables.

Local Service Facilities

The term "local service" implies the operation of getting outgoing telegrams from the originating party into the main telegraph office for transmission, and the reverse operation of conveying the incoming telegrams from the central office to the addressee.

The casual sender of a telegram dispatches his messages in either one of two ways. He visits the nearest telegraph office or agency and hands his message over the counter, or he calls the telegraph company on a telephone and gives his message to an operator in the telegraph office. Telephoned messages may originate either in a private or pay-station telephone, or in a "self-service" booth maintained by the telegraph company in a hotel lobby, railroad station, or other public building.

If the customer is a business man who has fairly frequent need of telegraph

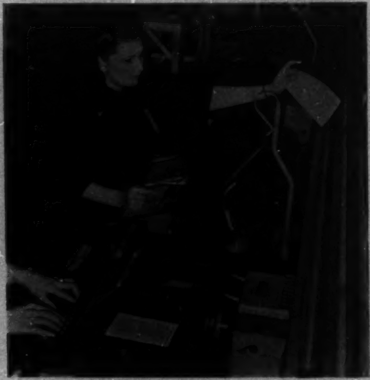

Figure 3 (left). V belts back of multiplex positions

Figure 4 (right). Typical branch office with pneumatic tubes to main office service, the telegraph company will have provided him with a messenger call box, by which he can call a messenger who will collect his message and carry it to the telegraph office or the nearest branch office. When his business requires regular use of the telegraph and more frequent service, the patron will have a direct private wire to the telegraph main office. This will probably be operated by a printing telegraph machine, or perhaps by telephone. Some of the more recent installations employ Telefax. The use of Morse telegraphy has been largely superseded for this as well as all other classes of service.

Whenever messages are handled through a branch office or an agency there is the extra operation of sending to the central office. This will usually be done by telegraph. In large cities, however, branch offices which are reasonably close to the main office will have pneumatic-tube connections; in some cases the transmission between branch and main offices is by telephone or Telefax.

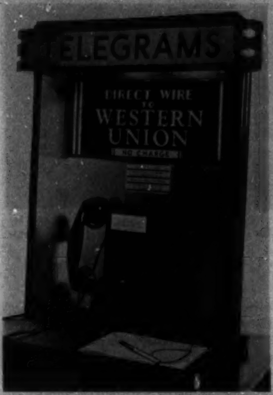

Figure 5 (above). Self-service telegraph booth

Figure 6. Tube-terminal room, concentrating incoming carriers, and time-spacing outgoing carriers

COMMERCIAL OFFICES

Of the various facilities mentioned previously, the telegraph commercial offices and agencies are of first importance to the general public. There is always a public commercial office in the same building as, and directly associated with, the main operating office. In larger towns and cities there are additional commercial branch offices located in other business or residential districts. Present trends are toward increased use of small branches and agencies in order to provide greater convenience for the public.

A typical branch office is shown in Figure 4. The equipment of such offices, besides the customary counters and public writing facilities, consists essentially of means for conveying messages to and from the main office, a telephone, time stamps for marking the arrival and delivery time of messages, and often a call circuit register for identifying the code signals sent from call boxes. In the office illustrated the connection to the main office is via pneumatic tube.

Telegraph agencies are, in effect, simply miniature commercial offices located in drug stores or other places which people frequent and are equipped with such facilities for communicating with the telegraph main office as best suit the local conditions. A telegraph self-service booth for telephone operation is illustrated in Figure 5. This equipment is often employed at offices or agencies during periods when no attendant is on duty.

PNEUMATIC TUBES

For many years pneumatic tubes have constituted one of the most generally used means for transmitting messages between branch and central offices, and also between different departments in the larger central-office buildings. Tube connections have the advantage that the original copies of messages can be transmitted, and that no special training is required for their operation. They also have a very large message carrying capacity. Since individual carriers hold as many as eight messages at a time, a single tube can handle as much business as several telegraph or telephone circuits.

The Western Union Company has nearly 400 miles of underground pneumatic tubes located in 53 different cities. The largest installation, serving 47 branch offices, is in New York. The longest tubes extend underground from the main office at Hudson and Worth Streets to Broadway and 40th Street, a distance of 3.3 miles, but they are operated with a relay at 23d Street. As a rule, however, tube lengths greater than 3,000 feet are not desirable because of the greater power required for operation and the longer transit time for the carriers.

Each tube line consists of a pair of tubes, $2^1/_4$ inches inside diameter, connected to a sending and receiving terminal at each end. The pair is customarily operated as a closed loop, the outgoing tube from the main office being under pressure and the incoming one under vacuum, with the air being constantly recirculated. In large installations air is supplied by large motor-driven compressors to manifolds which, in turn, feed all tubes of the system. In smaller jobs each tube has its own motor-operated blower, operated intermittently from a timing device. Messages are conveyed in "carriers," usually of fiber, eight inches in length. Transit speeds range from 1,500 to 3,500 feet per minute.

The terminals of telegraph pneumatic tubes are somewhat similar to those employed in department-store tubes for carrying cash and need no special explanation. Telegraph installations, however, do involve a number of unusual features. One is a hopper arrangement for concentrating carriers from several receiving tubes into a small number of outlets. Another is the provision of automatic timers to insure proper spacing of carriers in long tube runs and thus prevent pile-ups and possibly blocks (see

Figure 7. Page teleprinter mounted in console

Figure 8. Teleprinter concentration unit for 100 lines

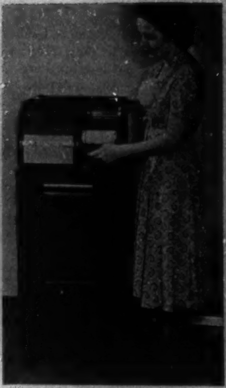

Figure 9. Telefax equipment for customer's office

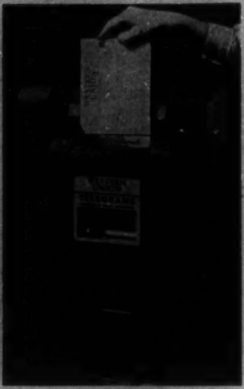

Figure 10. Slot-type Telefax transmitter

Figure 6). In special situations increased utilization of a tube is obtained by equipping its outer end with an electronically controlled selector by means of which carriers from the main office are automatically switched to any one of two or three different terminals, according to the type of carrier used. A special device in the New York installation is a silica gel dryer for drying the air in the tubes in the event that tube leaks or condensation from damp weather should tend toward water accumulations.

Although pneumatic tubes always have been considered one of the most efficient means for handling telegrams between main and branch offices, the recent development of central-office reperforator switching systems, which will be discussed subsequently, is causing a trend away from pneumatic tubes to teleprinter operation of branch offices. Under this system transmission from branches can be switched directly to trunk lines with the elimination of central-office handling and with an increased speed of service.

MORSE TELEGRAPHY

There is little to be said regarding Morse systems because they have been superseded so largely by automatic methods. The Morse system is still employed for occasional local operations, but its present-day use is largely confined to tributary circuits, such as those to railroad station offices and small towns.

The Morse circuits which still remain are operated either "single" or "duplex", depending on local conditions and requirements. Where a number of single Morse wires enter an office, it is customary to terminate them in a concentrator which provides means for an operator from one position to connect to and operate any one of several wires. Generally only eight circuits are wired to a Morse concentrator.

TELEPRINTERS

The teleprinter is the most widely used means for operating circuits between main offices and branch offices, private-line patrons, or tributary points; also for point-to-point services in leased wire networks. The teleprinter is a machine which, in a single unit, comprises a keyboard resembling that of a typewriter for sending and a printing mechanism for receiving. The two units are independent except that they share a common motor drive. They can be operated separately and simultaneously, or they can be interconnected electrically, when desired, so that the receiver will copy the transmission from the keyboard to give a home copy. The keyboard and the printing mechanism each has a distributor associated with it.

The teleprinter utilizes a five-unit permutation code of signals for transmitting and receiving intelligence. However, in order to maintain proper phase between sending and receiving instruments, an extra impulse is sent before each letter to start the rotating distributors in proper relationship to each other, and another impulse at the end of the letter to stop both distributors in the initial position for the next character.

Teleprinters are of several types, but they fall into two general classifications, tape printers and page printers. The former prints on a gummed tape that must be pasted on to a receiving blank; the latter prints directly on the blank. From the standpoint of the telegraph company, the tape printer has a number of operating advantages and is usually employed in branch-office operations. It avoids the necessity of transmitting special code combinations for "carriage return" and "line feed" at the end of each line. It also permits the correction of errors by simply retransmitting the errored word to provide a "paste-over" correction, instead of having to resend the entire message to provide a corrected copy, as is the case with page operation. On the other hand, page printers eliminate the gumming operation and hence are favored in many customers' offices on account of their greater convenience. A console type of page teleprinter is shown in Figure 7. Tape models with associated gumming desks appear in a subsequent illustration of central-office installations.

Teleprinter circuits are operated either one way at a time or on a duplex basis. On duplex circuits separate printers may be used for sending and receiving, respectively, or a single printer may be arranged for "split" operation, with simultaneous sending and receiving.

TELEPRINTER CONCENTRATORS

A concentrator may be defined as an arrangement of apparatus by which a

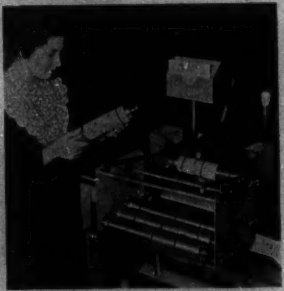

Figure 11. Telefax transmitter with removable drum and magazine feed

minimum number of terminal sets and operators can serve efficiently a maximum number of circuits.

Of the several types of concentrators which are in service, the most usual is a unit designed for coupling 100 teleprinter lines to any number of operating positions (see Figure 8). Each operating position, which carries one teleprinter and the usual accessories, is equipped with a panel or turret having 100 studs; all of the lines in the unit are multipled before each operator. The number of positions and turrets depends on the volume of business to be handled in peak periods over the particular lines associated with that concentrator. In concentrators handling principally circuits to branch offices, the number of positions may be anywhere from 10 to as many as 68 per 100 lines. Where the unit serves principally tie-line customers, the number is smaller, sometimes being as low as 8 per 100 lines.

The turrets are located conveniently, so that each operator may connect her teleprinter to the stud corresponding to any desired circuit. Lamp cabinets, containing a signal light for each circuit, give signals of incoming calls, and other signal lights furnish such indications as "operator not busy," "answer next," "all operators busy," and so forth. The system provides for calls to be answered by the lowest-numbered idle operator; it guards against more than one operator connecting to the same circuit; and it shows which group of positions is connected to any particular line, so that outgoing messages for that line may be directed promptly to that group. It indicates when too many or too few operators are on duty and provides for cutting out of service any position which may be released when a decrease in traffic permits.

Where traffic is particularly heavy it is not unusual to employ automatic transmission from the teleprinter tables. In such cases, each table is equipped with a keyboard perforator and a tape transmitter controlled by a standard start-stop sending distributor. Either the teleprinter keyboard or the tape transmitter may be used interchangeably.

In situations where a concentrator unit is connected to a considerable number of very lightly loaded circuits, printers are sometimes "double decked" for "receiving only" operation. As many as six printers may be used on a single table, the adjoining table being used for gumming purposes. In such installations, outgoing traffic is handled at other positions in the usual manner.

Telephone Recording and Delivery Equipment

The importance of a telephone department in a telegraph office is in direct relation to the proportion of total business which is telephoned in for transmission or is for delivery by telephone. At the present time this proportion is very substantial. In the telephone equipment of a central office the telephone lines are multipled to a series of turrets or switchboards, there usually being one turret for four operators—two on each side. Any of the operators who are not busy can pick up a call indicated by a signal light and record the message directly on a typewriter. In large installations, special signal lights and supervisory signals provide for answering calls in sequence and otherwise assist in maintaining prompt and efficient service. In order to reduce noise disturbance from telegraph operation, the telephone department is usually separate from and often in a different room from the regular telegraph operating department.

In general, telephone circuits to branch offices and private-line customers terminate in separate units from those which connect with the public telephone system. Where a considerable amount of business is handled by telephone, it is customary to have the recording and delivery of telegrams handled by separate groups of operators.

Telefax Service

The transmission by electrical means of facsimile reproductions of pictures, drawings, or text, is an art which has long intrigued the engineers and inventors of all countries. The principles are well known and comprise the scanning of small elemental areas of the copy and transmitting for each such area an electrical impulse, the character of which varies according to the relative darkness of the area. These impulses, as received; control marking means which traverse a film, paper, or other recording medium in exact synchronism with the transmitter. Thus, element by element, the recorder produces a replica of the image scanned by the transmitter.

Prior to the advent of electronic amplifying devices, the commercial usage of facsimile systems was extremely limited, but subsequently they have been employed extensively in the transmission of photographs for newspaper and magazine publication. The practical application of facsimile to telegraph communication purposes has always been impeded by the fact that the recording systems available involved either photographic development, chemical, or other processes which were deemed too costly or too slow for the general handling of telegrams and similar textual matter. Otherwise, facsimile transmission offered outstanding advantages in that it would permit the use of original copy, thus eliminating the necessity of typing out on a keyboard and the consequent chance of error.

Shortly before the war Western Union developed a facsimile recording paper which overcame these handicaps. This new paper, which has been designated by the trademark Teledeltos, is a coated conducting paper, light gray in color, which is marked black by the passage of an electric current through it. The paper is used dry and requires no processing of any kind, either during or after recording. It is not affected by light or ordinary moisture conditions, and produces immediately a clear-cut permanent record. Recording equipment is simple in that the amplified facsimile tone signals may be applied directly to the paper by means of a stylus riding continuously on the surface of the paper.

The Western Union Company's commercial application of facsimile has taken the form of Telefax, which first was introduced into local telegraph service in the

Figure 12 (left). Telefax concentrator unit for both sending and receiving

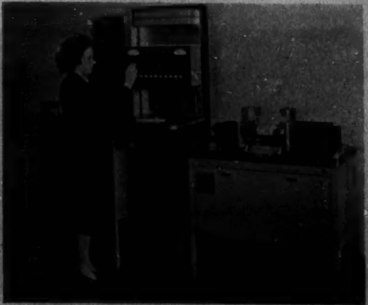

Figure 13 (right). Telefax concentrator for receiving only

cities of New York, Chicago, Atlanta, and San Francisco. Although extension of the Telefax program had to be interrupted on account of the war, the experience gained in these cities has demonstrated that Telefax will become an increasingly important factor in the future development of telegraph communications. It offers many advantages as a medium for communication between telegraph central offices and the general public. Already it has proved invaluable for the interchange of special data, forms, and so forth, between different offices and departments of industry.

Telefax Equipment

Because of its newness, Telefax design has not been stabilized. Several different types of machines already have been produced and are in service. All of these machines have been developed around the basic idea of providing maximum convenience for the general public. They require a minimum of human effort, time, and training for their operation.

The usual patron's Telefax is a combined transmitter–recorder (Figure 9) intended for both transmitting outgoing telegrams to the main office and delivering messages to the patron. For sending, the copy to be transmitted is wrapped around the extended horizontal drum; the send–receive switch is thrown to the proper position; and the starting lever is operated. All further operations are automatic. For delivery of a message, a buzzer signal operated from a central office announces an incoming message which is recorded by the patron placing a Teledeltos blank on the drum, throwing the send–receive switch to the proper position, and operating the starting switch.

For sending messages from branch offices, agencies, public buildings, or from patrons who have other receiving machines or arrangements for delivery, "slot-machine" transmitters offer the greatest convenience (see Figure 10). Several types of these machines have been developed; all, however, operate in the following general manner. The slot is normally closed. To send a message, a switch is first operated; then a signal from the central office opens the slot; the message is inserted; the slot automatically closes again, and transmission proceeds.

In branch offices, where fairly heavy loads must be handled, it is customary to use either the slot-machine-type transmitter just described or a table-mounted drum-type sending machine. In these machines removable drums, wrapped with the telegrams to be sent, are placed in a magazine from which they feed automatically to the operating position as fast as each transmission is completed (Figure 11). The receiving machines are of the same general type, only in their case the loaded drums are wrapped with Teledeltos sheets for recording. Another form of receiver records on a vertical cylinder around which the recording mechanism revolves. Teledeltos blanks in a continuous roll are fed to the cylinder and automatically cut off and discharged upon completion of each message.

At the central-office end of local Telefax circuits, transmitting machines are of the table-mounted drum type. Recorders are of either the table-mounted or vertical types, depending on local conditions.

Telefax machines for local use operate on a pair of wires with a ground connection for signaling purposes, and use 60-cycle a-c power synchronous with that employed at the central office to maintain synchronism between sending and receiving machines. In general practice the drums revolve at 180 rpm with a scanning rate of 100 lines per inch. This provides a transmission of 14 square inches per minute, although much faster and slower speeds have been used for special purposes.

Telefax Concentrators

Just as it has been found advantageous to terminate teleprinter circuits in central-office concentrators comprising a minimum number of operating positions, so has it been found desirable to install somewhat similar facilities for Telefax circuits. Separate recorders usually are provided for each branch-office circuit, but patrons' lines generally terminate in concentrators.

Figure 12 shows the concentrator used for combined sending and receiving on circuits to Telefax transmitter–recorders of the type shown in Figure 9. This unit accommodates as many as 25 lines and is usually installed with two transmitting tables and from two to four recording tables. The recorders are assigned to incoming messages automatically and released as soon as the recordings are completed.

Where the Telefax circuits are equipped with only transmitter units, such as shown in Figure 10, the central-office concentrator is of the type shown in Figure 13. This unit accommodates ten lines and has two recording tables associated with it. In order to secure greater flexibility, however, it is customary to interconnect between two such concentrators, so that any of the four recorders may be used on any of the 20 incoming circuits; in New York four concentrators are interconnected.

Trunk-Line Systems and Equipment

Line Terminals

All wires which enter a telegraph central office, whether for trunk or local service, run directly to and are terminated on a distributing frame. Those circuits which are exposed to possible hazard from lightning or other electric power source are first connected to protector apparatus. On this frame they are cross-connected to conductors which run to the switchboard or other office operating equipment. In small offices these installations may be combined in a single cabinet. In large offices the frames and switchboards are separate (Figure 14).

The protectors are assemblies comprising, in most cases, a line fuse connected to the incoming line, an office fuse connected to the office wiring, and an arrester between the line conductor and ground. The switchboards, built up from standard units to a size that meets the

Figure 14. Line-terminal switchboard in large office

Figure 15. Table-mounted telegraph repeaters

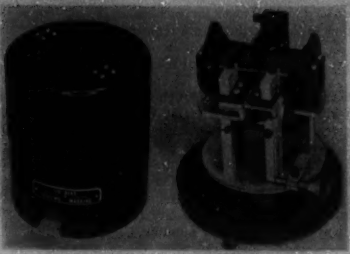

Figure 16 (left). Relay for use in high-speed d-c telegraph circuits

Figure 17 (right). Telegraph relay with plug base

Figure 18 (left). Reperforator

Figure 19 (right). Printer–perforator and transmitter, covers removed

local requirements, are interposed between the distributing frame and most units of equipment in order to provide quick means for testing lines and apparatus or for making temporary changes in the interconnections between various circuits and apparatus. Standard telegraph switchboards are essentially arrangements of jack panels, wiring, cord circuits and associated testing equipment, the jack arrangements and switchboard circuits being varied to suit the character of the particular service in which they are used.

D-C Terminal Sets and Repeaters

With the exception of carrier circuits and a limited number of d-c metallic circuits, the bulk of telegraph communication is carried on circuits with ground return, operated by d-c signal impulses. Polar transmission, using impulses of both positive and negative polarities, is standard for all trunk-line operation because of its freedom from bias troubles and its lower susceptibility to interference than single current transmission. Operation is usually duplex, affording simultaneous transmission in opposite directions.

Numerous types of terminal sets and repeaters are necessary to fulfill the varying needs. Terminal sets comprise the artificial lines and other apparatus necessary in duplex telegraphy for segregating the incoming and outgoing signal impulses, and also the means for converting between "polar" line transmission and the "make and break" signals of the transmitting and receiving devices. They also provide means for connecting to one or more branch lines or local stations, as the situation may demand.

Other types of repeaters are intended primarily for use at intermediate points in long circuits to strengthen the electrical impulses, to correct for signal distortion, or to permit connection to intermediate stations. Such repeaters also serve the important function of sectionalizing long lines, so as to localize disturbances and facilitate circuit patching for the restoration of service following line failures or interruptions. Repeaters in trunk service usually are spaced about 250 miles apart. A typical repeater installation is shown in Figure 15. Repeaters are of various types, some of them being designated by such names as combination duplex–duplex half repeater, terminal duplex–duplex half repeater, high-speed polar duplex, and high-speed single-line repeater. There are also other special types of which mention will be made later in connection with multiplex operation.

The all-important operating elements of telegraph repeaters are the relays which receive the electrical impulses and pass them on. In Figure 16 is shown a type of relay which is used in high-speed circuits. A relay typical of others that have important applications is shown in Figure 17.

In operating very long circuits, the distortion of signals, after they have passed through several sections of line equipped with ordinary types of repeaters, may become so great that it is necessary to employ some form of regenerative repeater which will restore the signals as nearly as possible to their initial form. Repeaters of this type usually comprise a rotary distributor with associated equipment, which receives the distorted signals and stores them up for retransmission from a sending distributor. Later types have been developed, however, in which electronic means for storage and distribution are employed instead of the mechanical distributors with their rotating brushes and storing relays.

Another form of repeater which has many applications is what might be called a storing repeater—a means for receiving and recording telegraph signals and automatically retransmitting them afterward. The usual method consists of setting up the received characters on a perforated tape from which they can be retransmitted as soon as the outlet circuit is available. The usual type of reperforator, as shown in Figure 18, simply perforates tape with holes corresponding to the code of the characters received. There is another type of reperforator which consists of an attachment to tape teleprinters permitting the perforating of a second tape at the same time the message is printed on the printer tape. A more elaborate mechanism is a printer–perforator which uses a somewhat wider paper tape and not only punches the code holes for each character but also prints the characters in a row just above and in register with the perforations (Figure 19).

Morse Operation

It is hardly proper to speak of Morse systems as a phase of trunk-line operations, because this type of telegraphy serves such a small percentage of present-day telegraph business. However, there are still many small communities having an only telegraph outlet via the same system of telegraphy that was in vogue long before the development of more modern automatic systems. Circuits in this kind of service are commonly regarded as tributaries and need no further description.

TELEPRINTER TRUNKS

Direct teleprinter operation of trunk circuits in the Western Union is limited principally to circuits which are of only moderate length and which carry light traffic. The fact that teleprinter operation will not utilize the full transmission capacity of a circuit is of little importance in such situations.

Teleprinter operating tables for trunk service are not greatly different from those used in local service, as shown in Figure 8. The printer equipment in use for any particular installation varies with the local conditions. Sometimes a single printer is used for both sending and receiving, "split" operation being employed when sending and receiving are simultaneous. Sometimes certain tables will be assigned to receiving, while others handle transmission; in other cases a single table may carry two printers, one for sending and one for receiving. In some situations, instead of direct keyboard transmission, a manually operated perforator is used for preparing perforated tape and the message transmitted automatically, a distributor-transmitter unit being employed to send the signals to the line.

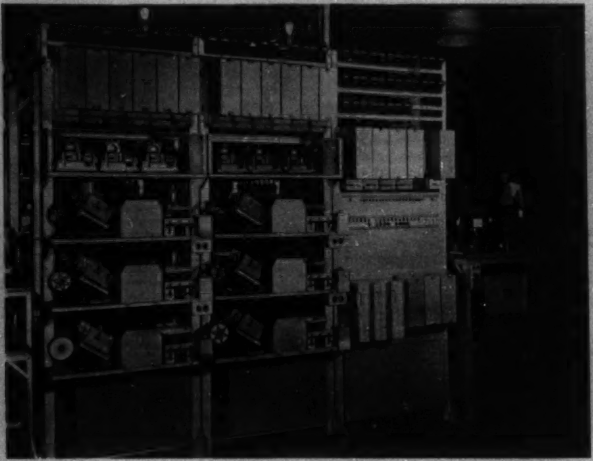

Figure 20. Varioplex terminal installation

MULTIPLEX SYSTEM

Since the average line-wire circuit and associated terminal and repeater equipment will carry signals much faster than they can be transmitted by an operator from a teleprinter keyboard, full utilization of circuit capacity can be attained only through the use of some system which will send signal impulses as rapidly as the circuit can transmit them. The system for accomplishing this is known as the multiplex and is used throughout the country to handle the major portion of trunk-line traffic.

The operating principle of the multiplex consists of dividing the use of the circuit equally among a number of separate channels by means of synchronously operating distributors or commutators at the two ends of the circuits. With this arrangement the signals from each channel transmitting at full speed are interspersed between the signals from the other channels, so that if enough channels are provided the whole circuit capacity will be utilized. Synchronism is obtained first by driving the distributors at closely regulated speeds from synchronous motors, the alternating current for which is supplied by carefully regulated tuning forks, and then by applying a precise phase correction controlled by the intelligence signals received over the line. Inasmuch as synchronism is maintained between the two ends of the circuit, it is not necessary to transmit the standard start-stop signals required for teleprinter operation. Hence, for each channel only five code impulses are required per letter as compared with the seven impulses required for standard teleprinters. Multiplex circuits may be operated with either two, three, or four channels, each duplexed, as line and traffic conditions may require. With four channels operating in each direction, giving four receiving and four transmitting terminals at each end, printer operation at the standard speed of 66 words per minute will provide a total message capacity for the circuit of 528 words per minute.

The operating tables associated with multiplex are ordinarily two-position units. In the receiving position the usual equipment is a tape printer with a gumming desk alongside of it; page printers have been used to some extent. For sending, a manually operated keyboard perforator with standard keyboard arrangement is used to prepare perforated tape which is fed through an automatic tape transmitter. This arrangement provides uniform and constant speed transmission, regardless of fluctuations in the speed of the perforating operator.

In order to effect maximum utilization and flexibility in the use of multiplex circuits, it has been necessary to develop numerous special types of repeaters known as multiplex channel repeaters. These are used to provide connections to intermediate and branch points, to make separate channels available for circuits to divergent points, and to convert multiplex signals into seven unit code or vice versa, for start-stop operation of teleprinters. One type of channel repeater permits repeating a channel of one multiplex circuit into a channel of another multiplex circuit; another type will repeat a multiplex channel to and from a teleprinter circuit. Other types designated as channel assigners permit two sending branches to share one multiplex channel, or a sender on a multiplex channel to determine selectively which of two distant receiving printers will record his transmission. In all types of channel repeaters it is usual to employ relays to store up one or two characters in order to provide a small overlap between systems which are not in synchronism. At a channel repeater the outgoing circuit must of necessity be as fast as or faster than the incoming circuit.

VARIOPLEX SYSTEM

The Varioplex is a system of operation which provides a fairly large number of point-to-point subchannels over a single-line circuit, with a maximum utilization of the full message capacity of the circuit. With the usual methods of channelizing, as employed in multiplex and carrier systems, whenever any channel is idle, its circuit capacity is wasted, since none of the other channels can take advantage of it. In the Varioplex, when a subchannel becomes idle, its portion of the line capacity is made available to the other working subchannels and thereby causes an increase in the operating speed of those channels. Each individual subchannel constitutes a point-to-point circuit which is available for use at any time, the speed of any one depending upon the number of other subchannels that are at that moment working, and varying continually as the working subchannels vary in number.

Each Varioplex subchannel terminates in a sending teleprinter and a receiving teleprinter, connected respectively to the sending and receiving legs from the line terminating equipment. Each subchannel sending leg terminates in the central-office Varioplex set in a reperforator, which thus permits outgoing traffic to be transmitted from the terminal at normal teleprinter speed, regardless of what the line speed may be. This "sent" traffic, temporarily stored in the reperforator tape at the Varioplex set, is retransmitted into the line circuit at whatever speed may be available at that instant.

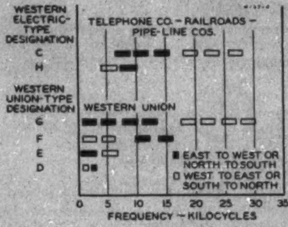

Figure 21. Frequency allocations of two-wire carrier systems

The several transmitters corresponding to the various subchannels are connected to the line circuit sequentially on a character by character basis by a relay-chain distributing arrangement, subject however, to a sensing circuit which will cause the sequence operation to skip any transmitters which have no traffic to send. A similar relay chain at the receiving end of the line causes the signals to be transmitted in proper sequence to the proper receiving teleprinters. An idle channel is "out of circuit" until it starts to send, at which time it is automatically "cut in" by the sending sequence, while a control signal is sent over the line to cause the receiving apparatus to follow the same sequence. As soon as a channel stops working it is automatically "cut out" of the sequence.

A Varioplex terminal installation comprises a distributor unit similar to that employed in multiplex, a control rack, and several reperforator racks, each carrying the reperforator and associated equipment for three channels (see Figure 20). Up to the present time Varioplex installations have been made only in connection with multiplex circuits, being designed to utilize anywhere from one to four of the separate channels of the multiplex. As many as 36 subchannels have been provided on a four-channel multiplex circuit, using a single wire, although the equipment is designed to accommodate a maximum of 50 channels if required. Varioplex channels may be repeated from one system to another to form through channels by simple interconnection of the channel legs of the two systems.

Although Varioplex has been utilized for the handling of regular telegraph business, it has found its widest application in providing service for telegraph patrons whose traffic is not great enough to justify the utilization of full circuits. This service, which is designated as "telemeter" service, gives the customer the equivalent of a full-time leased circuit, ready for use at any time, yet he pays for only the traffic actually handled, on a rate per word graded according to distance between termini. The word count used as a basis for billing is measured by character counters connected to the sending legs of each channel.

Carrier-Current Systems

Since it is the purpose of this paper to discuss only telegraphic applications and not theory, a detailed description of Western Union carrier systems is out of place. However, certain unusual features of these systems deserve special mention. As is well known, carrier transmission involves the use of relatively high-frequency alternating current, interrupted or otherwise modulated by ordinary d-c telegraph signals, the modulated current received at the distant terminal being reconverted into the original d-c signals. Through the use of separate carrier frequencies superimposed on a single pair of wires, a considerable number of independent channels or telegraph circuits may be obtained.

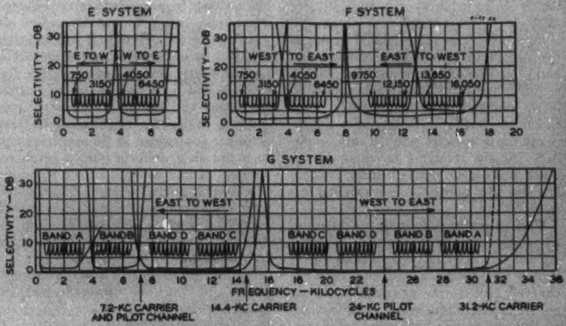

Figure 22. Frequency allocations to bands and channels

In all carrier systems the prevention of crosstalk interference is a major problem. The two wires of each pair must be closely alike and accurately balanced electrically; all pairs must be transposed according to a carefully worked out plan, the number of transpositions being roughly proportional to the highest frequency employed. In spite of these precautions it has been found desirable to effect further reduction in the crosstalk interference by co-ordinating the direction of transmission of frequency bands with respect to those of other carrier circuits. The allocations shown in Figure 21 have been adopted and are adhered to generally by all principal users of carrier transmission.

The earlier installations by Western Union Company were of a four-wire seven-kilocycle system, using one pair for transmission in each direction, with the same frequencies on each. All of the later systems are two-wire types with different groups of frequencies for transmission in opposite directions. Among these systems there is a so-called portable type designed for establishing short, temporary, or emergency circuits and a type *D* with a capacity of four channels designed to provide at reasonably low cost a small increase in facilities over distances not exceeding one or two repeater sections. Types *E*, *F*, and *G* supply 9, 18, and 36 two-way channels, respectively, for long trunk-circuit service. The frequency spectra and channel layouts of these systems are shown in Figure 22.

All three systems are built up with similar basic blocks of carrier channels, each block comprising a transmission frequency band of about 3,000 cycles. The *E* system has one block, the *F* system two, and the *G* system four for each direction of transmission. The basic channelizing arrangement provides nine wide-band channels, 300 cycles apart in each block, although prospective improvements will permit a tenth channel at 3,450 cycles. The effective band width of the channel filters is 160 cycles, providing high-quality telegraphic transmission up to 75 cycles per second—a generous margin over present standard multiplex speeds of 66 cycles (66 words per minute on each of four channels). For slow-speed operation, such as direct-operated teleprinter circuits, narrow band channels with a band width of 80 cycles and a channel spacing of 150 cycles have been provided. Two narrow band channels will replace one wide band channel and a block of channels may be made up exclusively of either type channel.

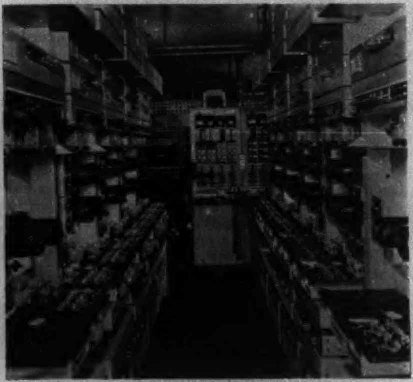

Figure 23 (left). Carrier terminal installation at Salt Lake City

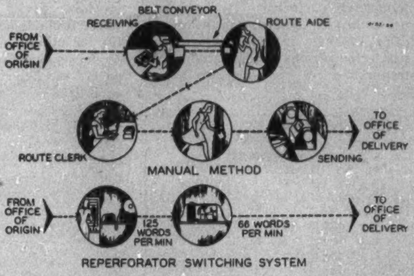

Figure 24. Message handling comparisons

Each channel terminal includes an oscillator which supplies the required channel frequency, the transmission of which is controlled or modulated by multiplex or other standard d-c telegraph equipment. The nine-channel group is transferred to its assigned transmission band by one or more modulations of secondary or translation frequencies, the latter supplied by a high-stability low-frequency oscillator from which appropriate harmonics are selected and amplified. At the receiving terminal the reverse process of demodulation restores the several channels to their original frequencies.

Until recently amplitude modulation, by which the carrier frequency is transmitted for marking signals and interrupted for spacing signals, was used exclusively for superposing telegraph signals on the carrier frequencies. This method of transmission is analogous to single-current transmission in d-c telegraphy and, like the latter, is susceptible to interference and to bias effects resulting from line attenuation changes or level fluctuations.

In the later Western Union carrier systems stability of operation analogous to that of polar d-c telegraphy has been attained through the use of frequency modulation. In this method the carrier-frequency generating oscillator is so controlled by the telegraph relay that the channel frequency is lowered 70 cycles from the normal midchannel value for a marking signal and raised 70 cycles for a spacing signal. Through the use of electronic control, the transitions between marking and spacing frequencies are effected gradually rather than instantaneously, thus resulting in true frequency modulation. The carrier currents at the output of the channel filter thus are of uniform amplitude, although they vary in frequency.

At the receiving terminal each channel is first separated from the others in the group by a receiving filter. The marking and spacing frequencies then are selected separately, rectified, and impressed on windings of the differential receiving relay, just as in d-c polar operation. The operation depends only on change of frequency and is independent of carrier amplitude variations. As an extra precaution, however, a current limiting amplifier is provided at the receiving end to maintain a uniform output level, regardless of any changes in the incoming level. The over-all result, as compared with amplitude modulation, is freedom from bias troubles and a substantial reduction in susceptibility to interference from all causes.

Restoration of energy levels is accomplished by vacuum-tube repeaters installed at intervals frequent enough to limit the average attenuation, under wet-weather conditions, to about 25 decibels between repeaters. This gives a repeater spacing about the same as for d-c telegraphy for the seven-kilocycle system, twice as frequent for the 16-kilocycle type-*F* system, and four times as frequent for the 30-kilocycle type-*G* system. In order to compensate for the increase of attenuation with increase in frequency, equalizer networks are provided at terminals and repeaters to effect a compensating attenuation of the lower frequencies. The final result is a substantially uniform attenuation within a carrier band, the magnitude being determined by the attenuation of the highest frequencies in the band.

In the practical application of carrier systems to telegraphy it is found that precautions must be taken to match the high-frequency characteristics when open-wire sections are joined to cable sections. It is therefore customary either to reduce the attenuation effects of cable sections by the insertion of inductance loading coils at intervals in the cable, or to eliminate discontinuities by impedance matching networks at the junctions between open wire and cable.

The equipment installations of carrier systems follow recognized practices of the communications industry, rack mountings being employed for all apparatus units, such as amplifiers, oscillators, filters, attenuators, and control panels. A typical installation is shown in Figure 23.

In the consideration of probable future trends in the application of trunk carrier systems, it will be evident that each 3,000-cycle transmission band will accommodate a telephone circuit, a high-speed facsimile circuit, or any other type of communication that may be adapted to this band width. The high quality of the Western Union system is amply demonstrated by its ability to carry high-speed multiplex circuits from coast to coast without requiring a regenerative repeater at any point. A measure of the ability of carrier installations to provide the circuits needed for future telegraph expansion is indicated by the fact that the type-*G* system, operated multiplex, will furnish on a single pair of wires 36 four-channel two-way circuits having a total message capacity of 9,500 words per minute in each direction.

TRUNK-LINE TELEFAX

From the previous discussion of Telefax service between main and branch or patrons' offices, it will be evident that the use of Telefax for trunk-line operations calls for little change from local practices. Transmitters and receivers may have to be somewhat more refined, and the trunk circuit must have good frequency transmission characteristics, so that unequal attenuation of different frequencies will not cause distortion in the received image. Otherwise, the principal difference between local and trunk facsimile operation is in the methods of maintaining synchronism between the sending and the receiving machines. In local service this is done by driving both terminals with synchronous motors supplied from the same a-c power network. In trunk operation the cities at which the circuits terminate frequently are served by power systems that are not interconnected, and other means must be provided to maintain synchronism. Of the several ways of doing this the one most commonly employed provides an independent accurately controlled source of alternating current for each terminal. Through appropriate amplifiers, quartz-crystal units, or tuning forks, operating under closely regulated temperature conditions, generate 60-cycle power which drives the Telefax motors.

To secure good-quality facsimile transmission at the speeds used in local service, a frequency band width of about 2,500 cycles must be employed. This means that the trunk circuit must be approximately equivalent to a telephone circuit or to one of the blocks of carrier frequencies provided in the carrier systems just described. Since such a block has a very large word-carrying capacity, when used for multiplex-equipped carrier channels, it will be evident that, if employed for facsimile, the message capacity will be greatly reduced. However, with the cost of suitable trunk circuits trending downward, as carrier development expands, there seems little doubt that the many advantages afforded by Telefax ultimately will result in its extensive application for intercity communications.

Figure 25. St. Louis reperforator switching office, switching aisle

Figure 26. Atlanta reperforator switching office, sending aisle

Development of trunk-line Telefax by Western Union has been limited to a few installations made primarily for the purpose of determining its advantages and limitations and for gaining experience in its operation. Circuits have been in regular operation for several years between New York and Chicago. Similar circuits were in operation between New York and Buffalo until war needs caused their temporary suspension. These Telefax installations have been employed for two distinct purposes. First, they have furnished a public facsimile service transmitting drawings, sketches, copy matter for publication, editorial corrections, and the like. This service has been supplied at rates based on the square-inch area transmitted. Second, they have carried commercial telegrams in regular commercial service.

Switching Systems

From the remarks made under the headings of "Traffic Considerations" and "Central Offices," as well as in numerous other places in this paper, one cannot escape the realization that the handling of millions of short telegrams between points scattered throughout the length and breadth of the country must require a complicated interchange of traffic at all offices where trunk lines meet other trunk lines or local plant. The operations are very different from those of long-distance telephone service in which through connections are established and usually remain operative for a period of at least a few minutes. In telegraph practice average messages contain only a few words and require less than a minute for transmission. It takes longer to handle them than to send them. Furthermore, consecutive messages usually will be headed for different destinations. The real problem at each transfer office or "relay" point is to sort out these messages and transfer them to the new routing without delay and without wasting a large amount of trunk "line time."

In ordinary methods of main-office operation an incoming message is first recorded on a tape and gummed onto a blank, checked for errors, stamped with the time, sent to the message distributing center, conveyed to a message drop close to the outgoing circuit, manually transferred to the operator of that circuit, and finally retransmitted toward its destination. The various steps are shown in the upper portion of Figure 24.

Since the birth of the telegraph efforts constantly have been directed toward speeding up this intraoffice handling. Many attempts have been made to accomplish the desired end through switching arrangements, but, in large offices where the number of messages handled daily runs into the tens or hundreds of thousands, it has not been found practicable to switch directly from a receiving terminal to an outgoing circuit. In such offices, the outgoing circuits are so busy that incoming business often may be held up, waiting for the outlet to become available. In order to avoid congestion on the incoming circuits and to allow the distant stations to send at any time at their own convenience, it is desirable that incoming messages be moved away as fast as they arrive and, if necessary, held elsewhere until they can be retransmitted to destination.

Figure 27. Nine-conductor jack and plug, five code wires, four control wires

REPERFORATOR SWITCHING

The Western Union solution for handling relay traffic is what is known as the reperforator switching system. In the large central-office installations of this system all receiving terminals are grouped together in one area and all transmitting terminals in another with intraoffice circuits connecting them. Each incoming circuit terminates in a printer–perforator which types the incoming message along the upper margin of a paper tape and perforates the signal code for the same characters below. Alongside the perforator is an automatic transmitter into which the tape is fed. Associated with each group of reperforator tables is a switchboard, or turret, with one or two jacks corresponding to each outgoing circuit and with a shelf in front on which are a plug and cord from each transmitter.

As soon as a message is received a switching clerk notes the destination and immediately connects the cord corresponding to that printer–perforator and its transmitter to the appropriate outgoing jack. Transmission then starts automatically to the unattended sending section of the switching system, where the message is again recorded on a reperforator (a standard type and not a printer–perforator) that is connected through a transmitter and certain switching devices to the outgoing circuit. If the circuit is idle transmission to destination starts at once; if it is busy transmission starts as soon as the circuit is available. The general scheme of operation is shown in the lower portion of Figure 24.

The intraoffice circuit operates at a speed of 125 words per minute, nearly double that of the trunk circuits, so it will be evident that traffic does not pile up at the receiving point and delay incoming business. If any holding is necessary due to congestion on the outgoing circuit, it takes place in the reperforator tape at the sending position and has no effect on other business passing through the office.

In reperforator switching installations, the receiving tables and turrets are arranged along the two sides of a switching aisle, the sending equipment being located in nearby sending aisles. Typical installations are shown in Figures 25 and 26. It is general practice to mount the receiving printer–perforators and the associated cord circuit transmitters on double-deck tables, one table each side of the switching turret. Trunk circuits usually are assigned four to a turret; tributary, branch office, and local circuits are assigned either four or eight to a turret, depending on loads to be handled during peak periods. In the sending section, reperforators and transmitters also are double decked with auxiliary equipment carried on the shelf above and in space below the lower shelf.

The system as a whole involves few really new inventions. It is more a regrouping and refining of well-known methods and devices to perform certain functions in a new and better manner. Perforated tape for automatic transmission and message storage is almost as old as telegraphy itself. If there is any one new item that has contributed most to the success of reperforator switching, it may be said to be the printer–perforator which simultaneously prints type and punches code holes on a paper tape. Another important feature is the high speed in the intraoffice circuits, obtained in present installations through the use of multiconductor cords, plugs, and circuits which carry five-unit code impulses simultaneously on five separate conductors (Figure 27).

There are many special features which have been provided to speed up or facilitate operations and safeguard traffic. Complete sets of routing charts are mounted over each receiving table to enable switching clerks to determine the assigned routing for all business, both that destined for outside cities and localities and that addressed to nearby tributaries, customers, and branch offices. Then there are large, conspicuously located routing boards, with remote control, to post alternate routings when such are required. There are "circuit allotters" which automatically sequence transmissions on the cross-office circuits in the event that two or more switching clerks should simultaneously establish connections to the same destination.

Another important device is the numbering machine on each intraoffice circuit which automatically interposes a message number in the reperforator tape ahead of each message. These numbers, all starting from one, are in sequence to each distant receiving printer and are renewed for each day's business. The distant operator thus can check the receipt of all messages and identify individual telegrams in case of inquiry or error in transmission. A loud-speaker system interconnects between different sections of the operating room and the testing and regulating department to provide instantaneous intercommunication among switching clerks, supervisors, and maintenance forces.

Of course there are many branch and teleprinter tributary circuits which carry too little traffic to justify the expense for individual printer–perforator terminals at the receiving center. In such cases the lightly loaded circuits are terminated in special concentrator units in which four printer–perforators at one receiving position are made to serve as many as 12 separate lines. The operation is entirely automatic, a receiving unit being cut in circuit whenever an outlying station begins to send, and cut out again as soon as the transmission ends.

In addition to the facilities employed solely for switching telegrams through an office, there is also a local section which handles the necessary conversions between the automatic transmission of the switching unit and the written telegram of the customer. The local section consists of regular table operating positions, like those of a manual office, equipped with teleprinters. These are used for sending into the switching center the outgoing messages which have reached the operating room by messenger pickup, telephone, Telefax, or pneumatic tube, and for receiving and recording from the switching center the incoming messages which are to be delivered through the same media.

Other important sections of the switching office are the "spillover" and *XV* centers provided for handling unusual situations. Any messages which cannot be dispatched immediately to their destinations because of line trouble, office closed, uncertain routing, incomplete address, and so forth, or specially urgent messages of governmental or emergency nature, may be switched to these centers for the special handling that may be required. Both spillover and *XV* centers comprise switching turrets and printer–perforator receiving positions of the same general design as used for regular business.

The Western Union has reperforator switching installations in operation in Atlanta, Dallas, Fort Worth, Richmond, and St. Louis; another is under way in Oakland, Calif., and three other large ones are in the early stages of preparation. In Washington, D. C. there is an installation of "partial" reperforator switching designed for switching outgoing messages to certain groups of trunks while retaining normal methods for other traffic handlings.

In all installations, to date, the results of operation have been even more successful than anticipated when the system was first developed. Trunk-wire facilities terminating in reperforator switching offices handle 15 to 20 per cent more traffic than under the old system, and the speed of message handling is more than doubled. Switching clerks can be trained in one tenth of the time necessary to develop proficient keyboard operators, and these clerks will handle three or four times as many telegrams as operators in a manual office.

Postal Telegraph Company Switching System

Quite distinct from the Western Union system, and yet designed to serve the same end of speeding up central-office handling of telegrams, is the system developed by the former Postal Telegraph Company and first installed in 1941. Both switching systems use printer–perforators for receiving telegrams on tape and automatic transmitters for sending on the outgoing circuits, but they differ in the manner of sorting out and routing the messages and in getting the outgoing ones to the proper transmitters.

Instead of performing the routing at a switchboard turret and making the necessary transfer electrically through the medium of a plug and jack, the Postal Telegraph method involves separating the received printer–perforator tapes into short lengths, one for each message, passing the pieces of tape to special routing clerks who mark the routing on each piece, then transferring the tapes to sending operators, each of whom has charge of a group of transmitters for various circuits. These operators feed the tapes into the proper transmitters for transmission. The transfer of tape is handled manually in the smaller offices, but in large installations the transfer from the routing desks to the sending tables is accomplished by pneumatic tubes.

In this system advantage is taken of tape reception, automatic transmission, and of the fact that individual sending and receiving operators can attend to more circuits than can a teleprinter or multiplex operator in a manual office. The use of automatic transmission also eliminates the chance of operator error in keyboard transmission.

The reception of telegrams in the Postal Telegraph system takes place in receiving cabinets which are metal enclosures, each housing eight printer–perforators in a double-deck assembly (Figure 28). Except under heavy load conditions, a single operator can attend one or more receiving cabinets. Each incoming trunk line terminates in a printer–perforator. Branch-office lines, however, are connected to the printer–perforators through line-finder units in which as many as 50 lines can be associated with 16 printer–perforator positions, each capable of reception from any one of the 50 lines.

The sending tables are located in another section of the operating room, each table being attended by one operator and carrying up to 12 transmitters mounted in banks of six (Figure 29). In the case of teleprinter circuits which operate on a start–stop basis, transmission goes direct to the line. When operation is by multiplex, transmission, as in the Western Union system, goes first to an unattended reperforator, the tape from which feeds into the line transmitter. With this arrangement, sequence numbers are inserted at the head of the message before it leaves the first transmitter. The cross-office transmission is at a speed of 65 words per minute.

Figure 28 (left). Postal Telegraph Company perforator-switching receiving equipment

Figure 29 (right). Postal Telegraph Company perforator-switching sending equipment

One of the equipment differences between the Western Union and Postal Telegraph switching systems is that the printer–perforator used in the latter is essentially a "typing reperforator," which uses a narrow tape and prints the characters right over the code perforations. In order to obtain readability, the holes in the tape are punched only part way, eliminating the "chad" and thus providing a complete surface to receive printing, although still leaving the holes free for entrance of the transmitter pins.

Another feature is a monitor or automatic channel rider to guard against improper operation of multiplex channel circuits. This device sends test signals at regular intervals over one of the multiplex channels; if the signals fail to arrive correctly, an alarm is given immediately.

Subcenter Switching

It sometimes happens that a considerable number of branch offices and private-line patrons are localized in an area quite remote from any important traffic center. The business of these stations usually will be handled from a local office, which then must transfer the business to the next traffic center which has trunk connections with distant cities. In order to reduce the message handlings, delays, and operating expense that occur in these situations, there have been developed a series of subcenter switching units for automatically connecting a group of stations to a small number of trunks, extending to the traffic center. The number of trunks will be determined from the maximum number of stations which may be expected to operate simultaneously—usually about one third of the total number of stations.

In this system the patron's teleprinter sends to the central office just as if there were a direct circuit, the subcenter making the connection to the central-office line automatically. Incoming business to the patron is handled by the central-office operator dialing the station number over one of the subcenter trunks, the subcenter automatically selecting the proper station and making the connection.

The equipment has been designed in various sizes to serve groups of 10, 25, 50, and 200 offices, the 10-unit size being shown in Figure 30. The largest units yet installed are at Newark, N. J., where branch offices and private-line patrons in that vicinity are connected directly to operating positions in the New York central office, and in Oakland, Calif., where direct connections to San Francisco are provided for 50 Oakland private-line patrons, using only 16 conductors in the cable across San Francisco Bay.

PRIVATE-LINE SWITCHING SYSTEMS

The establishment of extensive leased-wire networks to connect the offices or plants of large industries and governmental bureaus has created a need for switching facilities at central points of these systems to serve the same function as relay offices in the commercial system. However, since the number of circuits and outlets to be switched together is much smaller and the loads lighter, it has been practicable to develop much simpler switching systems than used in the Western Union offices.

In the larger private switching systems, reception is on a printer–perforator, and the outgoing connections are established by a cord circuit connected to the outgoing circuit just as in the commercial installations. In this setup, however, no cross-office circuits or secondary storage is provided, the transmission going directly to the line as soon as the circuit is clear.

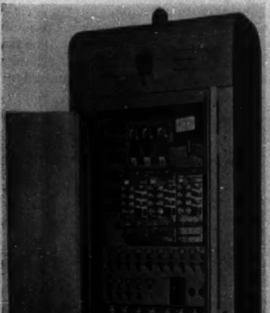

Figure 30. Subcenter switching unit for ten lines

A typical installation is shown in Figure 31. It comprises one switching turret divided into unit spaces, one for each outgoing circuit. In each space are a number indicator and two jacks. The receiving printer–perforators, each with its own cord-circuit transmitter, are housed in metal cabinets on each side of the turret. The cord circuits from all of the transmitters are located on the shelf just below the turret panel. Auxiliary equipment is housed in separate cabinets.

In operation, the switching clerk reads the destination on the tape and then plugs the cord circuit associated with that receiving terminal into either one of the jacks on the outgoing circuit. The two jacks are provided, so that if the circuit is already in use, the second jack may be employed for making the second connection. Immediately after the connection is made (or as soon as the circuit is released from the previously established connection), a numbering machine transmits the sequence number for that circuit, and then transmission starts. The number indicator on the turret panel shows the same number to be entered on the number sheet by the switching clerk.

Should line trouble, a closed office, or other cause prevent immediate transmission on an outgoing circuit, the tape is torn off and the delayed message placed in one of the tape clips just below the turret for later handling. If the delay is only temporary, as from a sudden peak in traffic to a particular destination, the unswitched received tape is not torn but is allowed to accumulate in the tank below the table without interrupting further incoming business. The turret shelf mounts an extra transmitter with its associated cord circuit for use in handling any business that may have accumulated for one reason or another.

Switching centers of the type just described have been provided for several of the largest private telegraph networks in the country. They have operated successfully and have improved both the accuracy and speed of message handling. One system, employing several of these switching centers, handles over three-million messages a year. At the time these installations were made consideration was given to the use of completely automatic means for performing the switching operations, but the manually operated switchboard turret was chosen as offering maximum simplicity, serviceability, and freedom from maintenance troubles.

Where the number of circuits in a private leased network is not too large, switching facilities have been provided which employ a separate switching turret for each incoming circuit, as shown in Figure 32. With this arrangement, it is usual to provide with each position a console-type teleprinter which makes a record copy of all messages relayed at the position. Outgoing messages, originating in the center, are prepared for transmission as printer–perforator tape at a teleprinter local position for subsequent switching to the outgoing circuits.

It is well to emphasize that no two private-wire networks operate under the same conditions or require the same kind of service. Practically every such system has to have a custom-built switching arrangement, especially engineered for the job. Hence, those just described represent merely typical installations. There is a wide variety of schemes for meeting the needs of the individual patron, but one feature which they all have in common is the use of perforated and printed tape for receiving telegrams, automatic transmitters for sending, and cords and plugs for making the connection to outgoing circuits.

Ocean Cables

All the continents of the world are interconnected by means of submarine cable. Across the North Atlantic alone there are 14 cables, with six more from North America to the Azores, where connections are made with Europe and, via the Cape Verde Islands, with Africa and South America. Of these 20 cables Western Union operates ten. Many cables also connect with Mexico, the West Indies, and South America. There are only three cables across the Pacific.

Transoceanic cables always have but one conductor except at the shore ends, where one or more extra conductors are provided to carry operating ground connections out to sea. Gutta-percha is used almost universally as insulation, and mechanical strength and protection are provided by armor of high-tensile steel. Recently, deproteinized rubber has been employed as a substitute for gutta-percha on cables manufactured on this side of the Atlantic for repair purposes, and a synthetic insulation of Polyethylene is now being experimented with. The new material offers much promise for the future.

The oldest Western Union cable was laid in 1873 and the latest in 1928. Earlier cables were of simple construction, but the more modern ones are loaded inductively by a spiral wrapping of high-permeability nickel–iron-alloy tape or wire surrounding the copper conductor. The new cables have a message capacity from three to eight times that of the older cables.

Western Union's 30,352 nautical miles of ocean cable lie in water ranging down to nearly three miles in depth. This cable is maintained by two repair ships which

Figure 31 (left). Private-line switching unit

Figure 32 (right). Single-circuit private-line switching turret

have $1,000,000 worth of reserve stock of repair cable to draw upon in case of emergency.

CABLE OPERATING EQUIPMENT

Because of the very high attenuation and phase distortion inherent in ocean cable, cable equipment and its operation until recently have differed radically from that used on land-line circuits. The transmitting voltage was kept low to lessen the danger of rupturing the cable insulation, and the primary receiving instruments were built on the d'Arsonval galvanometer principle to get the maximum sensitivity. Since the vibration period of these instruments could be adjusted to the signal frequency, they also served as shaping elements which, in association with condensers and additional inductors, could be made to provide a high degree of discrimination against disturbances of other frequencies.

In order to secure maximum utilization of cable capacity the conventional Morse code was modified, so that the so-called dots and dashes were of the same length but of different polarity. This produced a code length of only 3.7 units per letter, as compared with 9.2 units of the normal continental Morse. Reception was by means of signals traced by a syphon recorder on a paper tape which was read and translated by trained operators. Most of the world's cables are still being worked in this manner, although a certain amount of flexibility has been built into the system by means of regenerators, reperforators, multichannels, and printers that operate on this code. Operation is usually duplex, affording one transmission in each direction simultaneously.

In the Western Union operations the advantages to be gained through integrating the line and cable systems have resulted in radical changes in cable practices. Multiplex principles were applied to the loaded cables, providing as many as eight channels in one direction. They have since been applied also to the nonloaded cables. By means of so-called interpolating circuits, the five-unit code regularly used in land-line multiplex was made to function as a 2.5-unit code, yielding a net gain on the 3.7-unit recorder Morse code. Specially designed thermionic signal-shaping amplifiers made it possible to use rugged relays in the receiving circuits in place of the somewhat unstable and temperamental galvanometer type of instrument (see Figure 33).

With these developments it has been possible to equip Western Union cables with channel repeaters, reperforator apparatus, and Varioplex systems, which in their mode of operation are identical with the Western Union Company's land-line systems. Teleprinters in United States customers' offices are connected with similar teleprinters in customers' offices in Great Britain with the same facility that is possible within the United States. Channel switchboards permit the switching of channels, almost as independent circuits, and apparatus also is provided, so that two offices can share a channel if neither one has sufficient volume of traffic to keep it busy.

Because of the high cost of ocean cable, every effort has been made to secure maximum efficiency. Artificial lines for duplex balance are housed in constant temperature-constant humidity rooms in order to obtain great stability and have been developed highly as to design and construction. A new technique in matching the impedances of the artificial line and the cable largely has overcome the limitations of previous methods, still basically the traditional trial-and-error procedures, and produces accuracies of balance as high as one part in 50,000. The new method involves the frequency characteristic of the residual impedance unbalance and the application of resonant corrective networks which are readily calculated. A substantial reduction in the time required to obtain accurate balances has been achieved through the use of a cathode-ray oscilloscope of special design, as well as through certain auxiliary devices.

Subsidiary Services

Limitation of space precludes discussion of the various subsidiary services which were referred to in one of the early paragraphs. The following remarks accordingly are presented merely to indicate the magnitude of these activities.

Under normal conditions before the war nearly two thirds of the telegraph business which was filed in commercial telegraph offices was brought in by messenger. It will be evident, therefore, that in spite of many technical advances in the art of telegraphy the simple old-fashioned messenger call box continues to be one of the essential elements of commercial telegraph business. Upwards of 300,000 of these boxes are in service in patrons' offices, hotel lobbies, and other semipublic places.

In the furnishing of Western Union time service, by which naval observatory time is distributed to all parts of the country, a wire network over 200,000 miles in length is employed regularly. The signals distributed by the Navy pro-

Figure 33. Ocean-cable amplifier

Telegraphy in the Bell System

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Synopsis: This paper on "Telegraphy in the Bell System" is a companion paper for the one prepared by the representatives of the Western Union Telegraph Company on "American Telegraphy After 100 Years" and the one by representatives of the International Telephone and Telegraph Company on "Developments in the Field of Cable and Radiotelegraph Communications." This paper reviews some of the activities in telegraphy carried on by the various units of the Bell System in connection with the furnishing of private-line telegraph and teletypewriter exchange service. Frequent reference is made to the various publications covering these activities, and only those items which have not been disclosed previously are covered in any detail.

THE historic Washington–Baltimore telegraph message, "What hath God wrought?" preceded Alexander Graham Bell's conception of the electric telephone by about 30 years. In that interval much creative work in telegraphy was undertaken with results going far beyond the dreams of the inventors. It was while experimenting with apparatus intended to increase the number of telegraph messages that could be transmitted simultaneously over a circuit—a "harmonic" telegraph and a forerunner of modern carrier telegraph systems—that Bell verified his idea for the electric transmission of speech![1] The first basic telephone patent was designated "Improvement in Telegraphy," and many of the early telephonic experiments and demonstrations were made over telegraph wires. The close relationship which has always existed between these means of communication thus was strikingly begun.

The articles of incorporation of the American Telephone and Telegraph Company, organized and incorporated in 1885, contain a statement that its chief purpose was "constructing, buying, owning, leasing, or otherwise obtaining lines of electric telegraph, partly within and partly beyond the limits of the State of New York, and of equipping, using, operating, or otherwise maintaining the same." The term "electric telegraph" was interpreted to include both telephone and telegraph services as the name of the company implied.

One of the first undertakings of the newly formed company was the construction of a line from New York to Philadelphia which was soon extended to Washington. At that time, in addition to the public telegraph-message business in which responsibility is accepted by a telegraph company to transmit and deliver messages to designated parties, the telegraph companies were already furnishing some private-line or leased-wire service, and a demand soon arose for a similar service to be furnished by the telephone company. The telephone company's first private-line telegraph service was started between New York and Philadelphia on January 15, 1887, for a stock-brokerage concern, the line being equipped for both telephone and telegraph operation.

The telephone companies, as is well appreciated, operate on the basic principle of furnishing the instrumentalities (circuits and instruments) by which their customers can communicate satisfactorily. This same basic principle has been followed in the furnishing of telegraph service, first, under private-line contracts where a specific customer is provided with facilities to permit communication between specified locations for designated periods, and later, with a nationwide switching system known as teletypewriter exchange service (*TWX*). In the latter system a subscriber can request connection to any other subscriber or group of subscribers at any time. In all of these services the customers transmit their own messages, however, using arrangements provided for them. The stations, circuits, switching arrangements, and testing equipment used in furnishing these services have been designed to provide this private-line type of service, and, therefore, have taken on a form different in many respects from a telegraph plant intended primarily for a public message business. Under private-wire conditions customers immediately become aware of plant faults because of the interruptions of their communications. This makes necessary the provision of stable facilities and a means of quickly locating faults and means for the restoration of service. Transmission over individual circuit sections must be sufficiently good to permit interconnection of a large number to form a complete private-wire network and to permit rapid interconnection of any two stations or any group of stations in the country over teletypewriter-exchange-service networks. Likewise, teletypewriters and other station equipments must be arranged for connecting a large number on one circuit permitting breaking and two-way intercommunications.

The telegraph circuit facilities now provided from the telephone plant total approximately 4,000,000 miles. In addition to the circuits used for customer services, this total includes test wires, control circuits, and other telegraph circuits used in the maintenance and operation of the telephone plant. About 34,400 stations are served by these telegraph facilities (see Table I).

To illustrate the changes which have

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vide a means for accurate setting of some 2,000 master clocks which, in turn, distribute synchronizing signals to all clocks of the system.

There are over 20 different ticker services distributing quotations for various financial and commodity markets and news of sports and other happenings. The largest of these, distributing market quotations for the New York Stock Exchange, utilizes over 30,000 miles of wire and, at the peak of stock market activities, served about 8,000 high-speed tickers. A wire network interconnecting various cities and operating from a central transmitting station is also employed to serve the automatic stock quotation boards, operated by the Teleregister Corporation, a subsidiary of Western Union.

Some of the wire networks utilized in leased private-wire systems are also very extensive. A circuit used to collect and distribute weather information for the Civil Aeronautics Authority is over 32,000 miles in length and serves 670 stations, 200 of which are equipped for sending to all of the other stations on the circuit. One of the wire networks used by private industry comprises, besides several switching centers, over 6,000 miles of circuit. A news distributing network that is commonly known as the "Round Robin" is a continuous loop 4,365 miles long, serving numerous stations, any of which may transmit in both directions to every other station.

Conclusions

In the foregoing pages an effort has been made to outline the ways and means by which commercial telegraphy is conducted at the end of its first century. In all of its ramifications and services the telegraph of today is the outgrowth of numerous cycles of development. The most outstanding technical advances, however, appear to have been made within the last quarter of its history. This progress is by no means ended but is going on today perhaps more rapidly than at any time in the past. The engineering laboratories are working on many new developments which ultimately will play their part in further improving the nation's telegraph service.

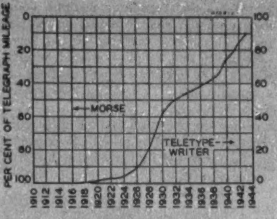


Figure 1. Relationship of the mileage of private-line Morse and teletypewriter services

occurred in this telegraph plant over the years, several diagrams have been prepared. Figure 1 illustrates the gradual change from Morse operation to teletypewriter operation in the private-line services. Figure 2 illustrates the multistation aspects of these private-line services. It will be noted that of the circuits shown 346 are point to point, that is, they have only two stations, whereas, all of the others have more than two stations up to a maximum of 329 stations on a single circuit. This figure also illustrates the distribution of these same circuits by lengths, the longest circuit having a total of over 30,000 miles. The changes in the *TWX* system, since its beginning, are shown in Figure 3.

Transmission Theory and Practices

The close association of the development of telegraph transmission systems with the art of telephone transmission is brought out in many publications.[2–6] Telegraphy differs from telephony, however, in that the intelligence transmitted is essentially conveyed by the timing of sudden transitions from one steady state to another. This, to a large extent, has required special consideration of the telegraph transmission problem in order to secure the efficient and faithful transfer of such transitions from the originating station to the destination.

Table I. Statistics Showing Mileage and Subscriber Stations

	Miles of Telegraph Circuit*		
	Carrier	Direct Current	Total
Cable	1,700,000	900,000	2,600,000
Open wire	900,000	500,000	1,400,000
Total	2,600,000	1,400,000	4,000,000
	Stations Connected*		
	TWX	Private Line	Total
Teletypewriter	16,000	17,000	33,000
Morse		1,400	1,400
Total	16,000	18,400	34,400

*These data include facilities for company communications as well as for customers.

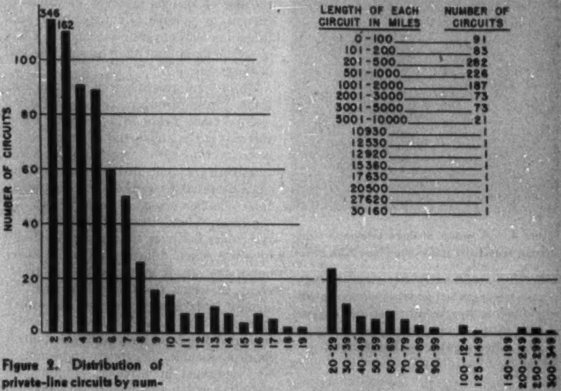


Figure 2. Distribution of private-line circuits by number of stations and mileage

Evaluation of Transmission Performance

In order to place the research and development work on a quantitative basis it was necessary first to formulate suitable definitions for the different kinds of transmission impairment suffered by the telegraph signals in transit. Inaccuracy in the timing of the transitions of the telegraph signals is the essential feature which determines the degree of reduction in the quality of the received signals. Accordingly, it was logical to state the impairment in terms of time displacement of the transitions from their respective proper positions in the train of signals. In 1927 a paper was published[7] defining impairment, specifically called "distortion," of telegraph signals, the imperfection with respect to time being expressed in per cent of the perfect (that is, undistorted) unit pulse or signal element peculiar to the code and speed employed. Furthermore, it had been found of great utility to break down the total distortion into three components, namely, bias, characteristic, and fortuitous distortion. It is interesting to note that these terms appear to have met with general acceptance throughout the world.

In Morse operation distortion was defined and measured as the combination of the effects which occur at the two ends of a mark or space.[8] However, the introduction of teletypewriter (start–stop printing telegraph) operation, commencing about 30 years ago[9–12] required modifications in the definitions and in the measuring technique. In start–stop teletypewriter operation each character consists of a start pulse, a selecting period five time units in length, and a stop pulse. The selective mechanism of the receiving machine begins its cycle upon arrival of the start pulse and makes an examination of the received signals in the middle portion of each of the five selecting time units. Accordingly, time distortion is properly referred to the beginning of the start pulse and considered independently for each transition during the selecting period. Teletypewriter operation has the important advantage of allowing convenient measurement of distortion on working circuits since the sent signals are accurately timed.

Several methods of measuring telegraph distortion which had been developed were described in the 1927 paper;[7] another paper published in 1939[13] outlined modifications found necessary in the definitions and described newer methods of measuring distortion, including a convenient and accurate start–stop device giving direct indication of bias and total distortion on working teletypewriter circuits. An improved transmission measuring set[14] employing an electronic distributor was standardized a few years ago. A group of three sets is shown in Figure 4, one of which is the electronic type.

For use in laying out circuit networks in an economical manner which will be satisfactory from a transmission standpoint in furnishing telegraph service and

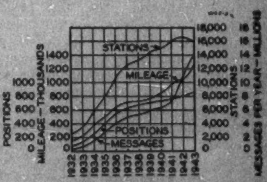


Figure 3. Teletypewriter exhange service

Curves showing the number of stations, messages per year, switchboard positions, and toll-circuit mileage

Figure 4. A group of three telegraph transmission measuring sets in the New York office

These sets have 30 meter appearances throughout the telegraph test rooms at service boards, testboards, teletypewriter repair centers, and so forth, at any of which teletypewriter signals at various speeds may be measured

in engineering additions to plant, a system of telegraph transmission "coefficients" was evolved.[15] In this system a figure is assigned to each link of a network in such a way that the coefficients for links operating in tandem may be added directly to determine whether or not the proposed circuit layout will meet the transmission requirements. Although these coefficients correspond in a way to attenuation expressed in decibels in telephone practice, they do not directly indicate the amount of signal distortion. They are based largely on the conception, which has been substantiated in practice, that distortion effects contributed by various factors generally combine in random fashion so that coefficients, as a first approximation, may be made proportional to the square of a representative value of distortion for each link.

When teletypewriter exchange (*TWX*) service[15] was introduced, the coefficient system proved to be of great utility in engineering a nationwide teletypewriter transmission system for providing a switched service which must be given over facilities connected together without time being taken for a line-up or test of transmission. In cases where the over-all coefficient would exceed the limit regenerative-type repeaters,[11,12] which not only reform but retime the signals, may be inserted, so that the circuit is effectively broken into parts for which the coefficients may be computed independently.

Transmission Principles

The fundamentals of electric telegraph transmission which have been evolved are, in general, applicable to such communication methods as telephone signaling and picture transmission[16] as well as to conventional telegraphy.

A study was made which led to a method of definitely evaluating the merits of telegraph codes differing as regards the combinations chosen to represent the various characters to be transmitted and the use of various numbers of values of current or voltage. A paper was published[17] in 1924 covering this and the effect of signal shaping at the transmitting end of the circuit.

Theoretical treatment of certain telegraph transmission questions was carried out for many years on a transient basis; this was fairly simple and of much utility in connection with forecasting the speed capabilities of nonloaded submarine cables, since these cables were so long electrically that reflection effects were negligible. In the cases of land lines and loaded submarine cables, however, the transient method[18] involved much difficulty. On the other hand, the steady-state method of treatment, in which the train of transmitted signals is broken down into sine-wave components by Fourier analysis, generally has proved to be of more utility and much more practicable. The steady-state method was described in a paper, published in 1928,[19] in which a number of fundamental conclusions also were recorded, particularly in respect to the required transmission band-width and transfer-admittance characteristics for distortionless transmission with the minimum band. It should be understood, however, that the transient and steady-state methods properly carried out lead to identical results.

The transmission capabilities of telegraph circuits described herein are, in general, such that teletypewriter operation at the predominant speed (60 words per minute, about 23 dot-cycles per second) is of high quality with considerable margin for variations in line conditions, battery voltages, adjustments, and so forth. This provides not only a high degree of dependability with small maintenance effort for normal speed services but also allows operation at materially higher speeds, up to about 40 cycles, in cases where this is desired.

D-C Systems

D-c telegraph circuits originally were derived from telephone circuits by what is known as the "simplex" method, commencing about 1882. In this system the telegraph currents traverse the two wires in parallel and employ ground return, whereas, the telephone circuit operates on a metallic basis. In 1892 the now widely used "composite" system[20] was introduced in which the telegraph and telephone currents are separated by taking advantage of the fact that the former employ low frequencies, and telephone currents are of higher frequency. By this method two d-c ground-return telegraph circuits may be superposed on a pair of wires employed simultaneously for telephony. It was necessary to develop a-c telephone signaling arrangements for use on composited circuits; these originally employed 135-cycle current, but later designs used modulated 1,000-cycle current.

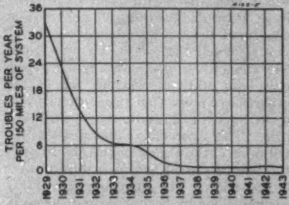


Figure 5. Curve showing the average number of system failures per year per 150 miles of voice-frequency carrier telegraph system, operated over cable

Up to about 1904 telegraph circuits on telephone wires were operated on a neutral or open-and-close basis. Several types of single-line repeater for use at intermediate points were developed at different times. The standard repeaters now in use[21] employ polar relays exclusively for better transmission with minimum adjustment.

In 1904 polar-duplex equipment was introduced for operation in private-line service, not so much for the purpose of providing simultaneous two-way transmission as for obtaining the advantages of polar transmission.[22] Later, one-way polar and two-path polar, that is, two ways on separate one-way paths, were introduced for use on many circuits to avoid the necessity of maintaining duplex impedance balance.

Various improvements and refinements were applied to d-c ground-return telegraph circuits, which operate largely over open wires. One of these developments consisted of arrangements for neutralizing the crossfire or mutual interference between such circuits operating on the same group of wires.[22] Another provided a ground-potential compensator[23] for neutralizing the effect of difference in ground potential between repeater stations caused, for instance, by the operation of a d-c traction system. Many of the repeaters were also arranged to employ a second or balancing wire so as to give effective metallic circuit operation and minimize interference. Two new forms of circuit, called "polarential,"[24] were introduced; these two arrangements are practically unaffected by changes in series resistance and leakage, respectively.

In connection with d-c telegraphy an outstanding development involving the joint use of plant consisted in the design of a low-current (a few milliamperes) polar system for long small-gauge cable circuits.[25] This development allows telegraph to take advantage of thousands of miles of small-gauge cable circuits which became available by the extension of intercity telephone cables. When the transmission was put on a metallic instead of ground-return basis, interference was practically eliminated, so that low voltages and currents could be employed and thus avoid objectionable interference in the composited telephone circuits on which they were superposed. The telephone circuits were particularly susceptible to infering effects by reason of the high amplification employed in repeaters and the large number of loading coils which would have been affected seriously[26] by sending through them d-c telegraph currents of the order of magnitude commonly employed for ground-return operation. The system originally operated on a two-wire polar-duplex basis, but later most of the circuits were converted to four-wire operation which is the equivalent of two-path polar.

Figure 6. Number 15 teletypewriter, arranged with positive paper-feeding arrangements for handling printed forms

CARRIER-CURRENT SYSTEMS

An important step in the improvement of facilities for furnishing telegraph service over long distances was the introduction about 25 years ago of carrier-current telegraphy. This was first applied to open-wire lines above the voice range and furnished ten two-way channels;[27] a few years later a voice-frequency system for operation on long four-wire cable telephone circuits was put into service.[28] The number of telegraph channels obtained per system was 10 initially, but this was increased to 12 within a short time, and subsequently to 18. A paper published in 1940[29] made record of a number of advances in carrier telegraph transmission, including a trial of arrangements which furnished 24 channels on a single four-wire cable circuit. Carrier telegraph circuits are not only more economical for all except comparatively short distances, but also furnish superior transmission. A simple amplitude-modulation (on-and-off keying) system with automatic level compensation is employed which is well suited for use on stable quiet circuits such as are available in the telephone plant.

Figure 5 shows the troubles per 150 miles of carrier system (regulator spacing) per year since 1929 and brings out the improvement in stability which has been brought about. The distortion is normally very small at usual teletypewriter speeds, and, furthermore, the circuits have some transmission margin which would allow an increase in speed; as a matter of fact, in certain special services, such as multiplex printer and ticker, considerably higher speeds have been used.

The general idea of a carrier telegraph system was old, but its practical development was made possible by drawing upon the telephone art, not only for a suitable transmission path, but also for such improved devices as electrical filters, vacuum-tube amplifiers, detectors, and oscillators.

It is of interest to note that voice-frequency carrier telegraph systems have been applied on a large scale to carrier telephone channels derived from both open wires and cable.[5,30,31,32,39,40] These facilities in general furnish excellent service and have been invaluable in making possible the use of certain very extensive networks. By making use of systems available in the plant about 260 of these two-way carrier telegraph channels can be derived from a single telephone pair. As shown in Table I there are approximately 2.6 million miles of carrier telegraph circuit in the plant today.

All of the carrier telegraph systems and a substantial part of the d-c systems now in the plant are operating on a two-path basis, that is, with separate paths in the two directions. These two-path facilities, of course, all can be operated both directions simultaneously (full duplex) where this is desired without an increase in signal distortion. The remaining mileage can largely be operated full duplex by maintaining a duplex balance.

As an essential feature of the improved telegraph systems, new and superior forms of demountable polar relays were developed.[33] These embodied such features as the use of permalloy magnetic material, contact arrangements for greatly reducing chatter, and high stability, so that adjustment is normally made only at intervals of several months to a year.

A paper published in 1922[34] described cables which were put into service between Key West and Havana in 1921. Each cable initially carried a voice circuit, a specially designed d-c telegraph circuit, and one carrier telegraph channel above the voice. Later additional carrier telegraph channels were provided.[35]

As a result of the study of transmission over long submarine cables, a new form of submarine cable was produced with continuous loading in the form of a permalloy (magnetic) tape. Equipment for automatic time-division printing telegraph operation, as well as vacuum-tube receiving amplifiers, was designed for use with this cable.[41,42] These developments have resulted in a large increase in the traffic carrying capacity of a transoceanic telegraph cable of given size and length.

Teletypewriter Developments

The advantages to be gained by the transmission of intelligence in printed form became apparent immediately following the 1844 demonstration of telegraph transmission by dots and dashes recorded on a paper strip. Many inventers worked on printing-telegraph arrangements, and gradually there were evolved certain requirements which should be met by such machines if they were to be useful on customers' premises in private-line telegraph services. These requirements which are met by the teletypewriters used today may be summarized briefly as follows:[12]

1. The operation should be simple, approaching that of a typewriter as closely as feasible. Only a limited amount of training should be required for an operator to become familiar with the touch and the arrangement of operating keys, including those necessary for controlling these machines over circuits. The insertion of paper, changing of inking ribbons, and the cleaning of type must be accomplished easily by operators.

2. Page copy is required; carbon copies should be made and stencils cut as with the ordinary typewriter.

3. Machines must be reliable in operation, reasonably quiet, of good appearance, and as small in size as feasible.

4. Machines must be economical to maintain in customers' offices and must not require routine attention for lubrication or adjustment except at long intervals. Means

should be provided for local tests, and parts should be readily interchangeable.

5. Positive feeding and tabulating arrangements are necessary for the handling of multicopy printed forms, such as orders and invoices.

6. Direct transmission from a keyboard must be provided, but to give efficient working it is also necessary to send automatically from some form of previously prepared record, such as perforated tape. Direct sending or perforating should be from the same keyboard.

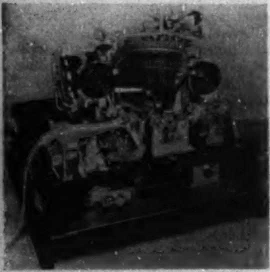


Figure 7. Typing reperforator–transmitter showing pivoted-arm type of transmitter

7. All transmitters must send accurately timed signals, and all receivers must have large tolerances for receiving and recording signals without errors.

8. Machine speeds of 60 words per minute and higher are required.

Furthermore, a selecting code is required which permits teletypewriters to be used in large intricate circuit networks, including many stations, with channels obtained from the telephone plant. In the private-line form of service any station may become a sending station, and the receiving devices of the various receiving stations must be phased automatically to receive properly signal waves arriving from any sender. This requirement led to the selection and development of the start–stop system with its break circuit which permits any receiving station to interrupt the operation of the circuit and become a sending station. The requirements included also the provision of features for remotely controlling the power supply of unattended machines.

The installation in 1917 for a press association of a start–stop page teletypewriter system on a circuit from New York to Boston, with a number of intermediate stations, is believed to be the pioneer long-line private-wire multistation teletypewriter circuit. This system had arrangements to permit breaking and fast intercommunication between stations. It formed the basis for the extensive development of long-distance start–stop private-wire teletypewriter service.

Although the idea of a stationary paper carriage for page-printing telegraph machines is not particularly new, the association of a moving type basket with a stationary paper platen to obtain the advantages of typebar action was first used in machines placed in service in 1921. This machine was known as the 10-*A* printer. This idea was developed later into a more satisfactory mechanism, known as the number 15 teletypewriter, shown in Figure 6, which was made available in 1930. Since then about 65,000 typing units of this kind have been manufactured by the Teletype Corporation.

The number 15 teletypewriter has met satisfactorily the requirements for a universally applicable page-printing machine as evidenced by the fact that it is playing a major role in telegraphic communications of both the Army and Navy in this country and also abroad, during the present war emergency.

The telegraph industry generally has adopted the use of capital letters instead of the usual combination of capitals and small letters. This has permitted the standardization of a five-unit selecting code, which gives 32 code combinations. A teletypewriter having a six-unit code with 64 combination possibilities and having capitals and small letters has been available for some time, but little use has been made of it except for special services due to the added complexity of the machine and less efficient use of the telegraph circuits.

In addition to the number 15 teletypewriter, many other teletypewriter mechanisms and auxiliaries have been developed and made available to meet the requirements of various commercial users. Some of these devices are illustrated in the figures showing commercial applications. Some not shown include a complete line of radio-frequency suppression filters for application to the various teletypewriter apparatus units to permit their use in close proximity to sensitive radio receivers. There are also the start–stop regenerative repeater, a transmitter–distributor for sending accurate test message signals without the use of perforated tape, a test set for sending miscellaneous distorted signals for testing apparatus and lines, a two-channel start–stop transmitter–distributor for use on radio circuits, a less elaborate page teletypewriter for limited applications where all of the service features of the number 15 are not required, and nonrecording selectors for automatic-switching purposes.

One of the latest mechanisms automatically records incoming signals in perforated as well as typed form for easy reading and has an associated transmitter mechanism mounted on a pivoted arm. This method of mounting the transmitter permits it to step along the tape character by character and transmit the last character recorded by the perforator. This reperforator–transmitter, which also has the unique feature of typing over the perforations, these being hinged lids, is shown in Figure 7. Either the reperfo-

Figure 8. Telegraph test room before the advent of rack-mounted equipment and telegraph board answering

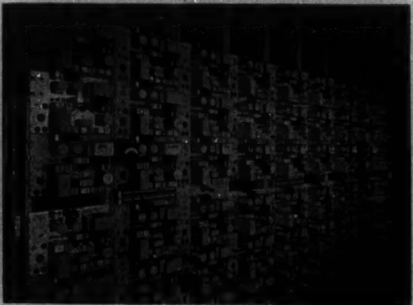


Figure 9. Voice-frequency carrier-telegraph-channel terminals

Six channels on each bay—three bays form an 18-channel system terminal

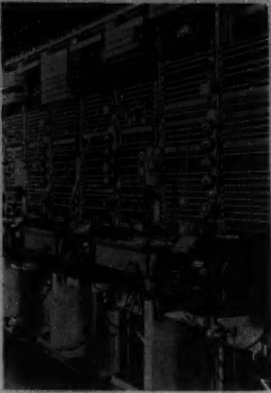


Figure 10. Operating positions and face equipment of widely used type of telegraph test and service board

rator or the transmitter may be run at either 60 or 75 speed, and the reperforator and the transmitter may be operated at different speeds in a combined unit. The transmitter also has a group of contacts to provide for reading certain characters from the tape for control of switching operations.

Improvement in Equipment Arrangements

Within the past 20 years revolutionary changes have been made in the mechanical construction and arrangement of the telegraph equipment installed in test and repeater offices. The contrast between the old and new arrangements is brought out in a striking manner by Figures 8 to 11. The first of these shows the appearance of a large test room about 25 years ago. The mahogany table in the foreground carries 12 d-c ground-return bridge polar-duplex repeaters; the table immediately in the rear mounts a similar group of duplex repeaters, and in addition six single-line repeaters on shelves above them. At the left may be seen the telegraph board which was provided primarily for making temporary rearrangement of line circuits and patrons' loops by means of patching cords. At the upper right is shown a telegraph operator's position on an elevated platform; this man sent and received over auxiliary facilities, known as test wires, short messages relating to the maintenance and restoration of telegraph service. Communication between him and the telegraph repeater attendants was carried out by shouting loudly so as to be heard through the din of many clicking sounders. It will be noted that a comparatively large staff is on duty.

Figure 9 is a view, recently taken in a modern office, of voice-frequency carrier telegraph terminal equipment. It will be noted that the apparatus is mounted on standard telephone-type racks, these being 11 feet 6 inches in height. A total of six or eight channel terminals may be mounted in each bay, $19^{1}/_{2}$ inches in width. A monitoring set is furnished for communication from the carrier telegraph equipment with patrons or other repeater offices, one such set being sufficient for serving 50 or more channel terminals. The monitoring sets normally are disconnected, so that no sounder is in operation. The relays are jack mounted, adjusted at comparatively long intervals at a central point, and replaced in case of difficulty rather than being adjusted while in operation.

Modern telegraph test and service boards are illustrated by Figures 10 and 11. The equipment shown by Figure 10 is rack mounted; that by Figure 11 is later in design, more efficient and of a switchboard type. At each board position a meter and jacks are provided, so that the attendant may make connection to a transmission measuring set and quickly determine the quality of teletypewriter signals on working circuits. Teletypewriters at each position permit communication with subscribers and with other offices when that method of communication is desirable. Monitoring teletypewriters, which are not shown, are generally grouped at a central point.

The new telegraph repeater arrangements are not only cheaper, but, because of the improved design and the use of manufactured assemblies, they have effected large savings in floor space, installation labor, and time. The test rooms are not only quiet, but also, with the new test and service boards, it has been found possible to maintain the service at even higher standards with a much smaller force. The reduction in the number of d-c ground-return circuits, the preponderance of carrier telegraph and widespread usage of circuits in cables, many of which are underground, as well as the use of better equipment and improved operating methods, have brought about improvements in spite of increases in operating speeds, more stringent service requirements, and the greater complexity of circuit layouts.

Private-Line Telegraph Systems

Probably the most outstanding change over the years in the private-line telegraph system is the application of a multitude of new ideas and new plant facilities for the improvement of the over-all stability of these networks. These improvements are not exclusively telegraph, but many of them have become available to telegraph as a result of its association of the same plant with telephony. Of course, the most outstanding example is the use of the extensive telephone cable plant for telegraphy. The requirements as regards speed, distance, and complexity have become more and more severe, but in spite of this there has been a continual improvement in the reliability and continuity of the service rendered. Many of the improvements have been presented to the Institute as they have been made, and it will suffice here merely to refer to a few of the more important developments.

In connection with the carrier telegraph developments, level compensators were applied which keep the output of each carrier channel practically constant with over-all changes in level on the circuit as large as ±10 decibels. Also, in connection with carrier, drainage arrangements[27] were designed after much experimenting to reduce the effects of lightning. High-frequency telegraph systems on open-wire and voice-frequency telegraph systems operating on carrier telephone channels, both type *C* and type *J*, are subject to

Figure 11. Operating positions and face equipment of a new form of telegraph test and service board

hits occasioned by lightning and other disturbances under the general name of static. Drainage has been applied to practically all carrier telegraph on open wires and has been effective in improving the telegraph service by reducing the hits which otherwise would have been experienced by 80 or 90 per cent.

The change from open wire to cables, which is continuously being made in the telephone plant, of course, has improved the stability of the telegraph facilities. This applies to the coaxial cables as well as to the usual small-gauge copper-conductor cables. The use of this plant has resulted in a service largely impervious to storm and weather. The improvement in the regulation of power-plant voltages has had a marked effect in the improvement in telegraph circuit stability. Practically all power plants used are equipped with storage-battery reserves, and the voltage is held within ±4 per cent of the nominal voltage or better. The storage-battery reserve, of course, provides continuity and in many cases a further assurance of continuity is obtained by the provision of emergency gas or Diesel-engine generating equipment. Mobile generating units are strategically located to protect plants not having permanently installed emergency equipments. These developments practically have eliminated telegraph circuit interruptions because of failures of the commercial power supply.

The application of the start-stop regenerative repeater throughout the telegraph plant has done away with any practical limitation on the length or complexity of telegraph networks which can be operated satisfactorily.

Improvements in operating methods have kept pace with improvements in facilities. Under what is known as the telegraph-board answering method of operation, the great majority of work in telegraph test rooms is performed by attendants seated at the telegraph testboards or service boards. Under this method of operation an attendant has at his position facilities for communicating quickly with customers and with attendants in other offices. He also has facilities for locating troubles and for quickly restoring interrupted services by patching out defective parts of circuits. Communications between attendants in different cities are

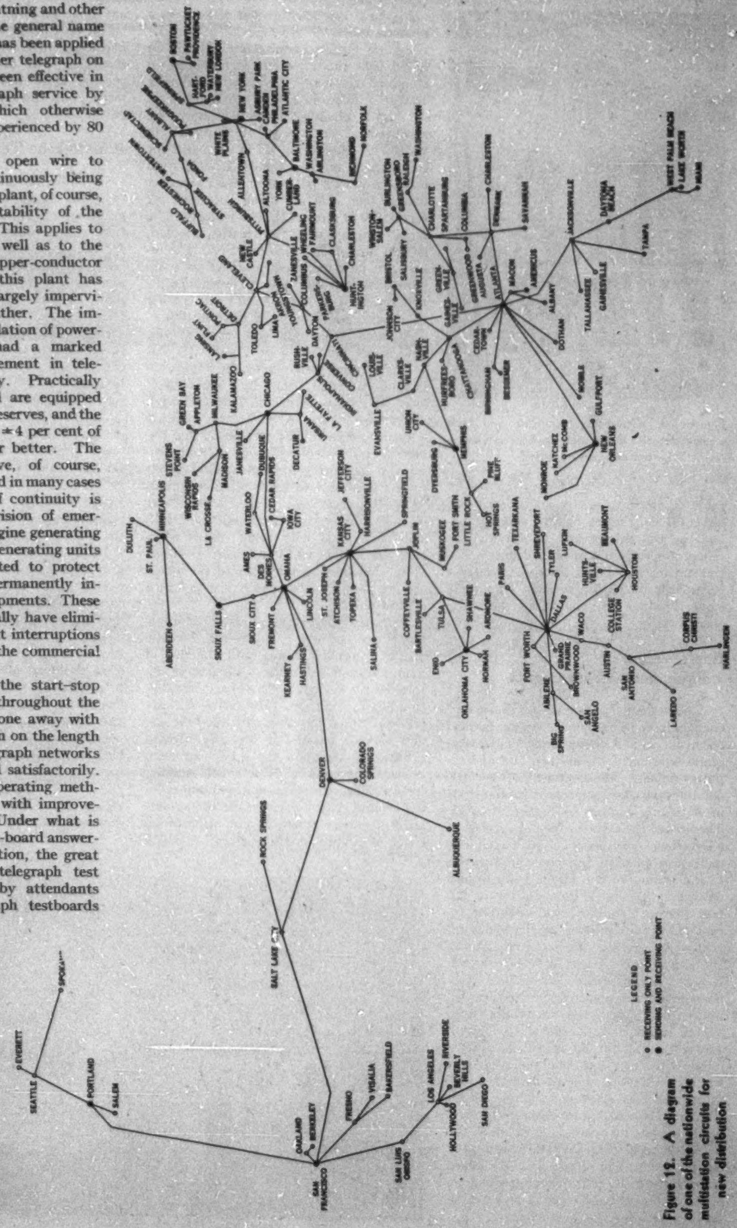

BOSTON
PAWTUCKET
PROVIDENCE
HARTFORD
WATERBURY
NEW LONDON
NEW YORK
ASBURY PARK
CAMDEN
PHILADELPHIA
ATLANTIC CITY
WHITE PLAINS
ALLENTOWN
BALTIMORE
WASHINGTON
ARLINGTON
NORFOLK
RICHMOND
ALTOONA
YORK
CUMBERLAND
HARRISBURG
PITTSBURGH
BUFFALO
ROCHESTER
SYRACUSE
ELMIRA
WATERTOWN
SCHENECTADY
ALBANY
BINGHAMTON
UTICA
NEW CASTLE
CLEVELAND
YOUNGSTOWN
WHEELING
ZANESVILLE
FAIRMONT
CLARKSBURG
CHARLESTON
HUNTINGTON
COLUMBUS
PARKERSBURG
DAYTON
LIMA
TOLEDO
DETROIT
PONTIAC
FLINT
LANSING
KALAMAZOO
CINCINNATI
RUSHVILLE
INDIANAPOLIS
LA FAYETTE
URBANA
DECATUR
CHICAGO
GREEN BAY
APPLETON
MILWAUKEE
STEVENS POINT
WISCONSIN RAPIDS
MADISON
LA CROSSE
JANESVILLE
DUBUQUE
CEDAR RAPIDS
IOWA CITY
WATERLOO
AMES
DES MOINES
OMAHA
LINCOLN
FREMONT
SIOUX CITY
SIOUX FALLS
ABERDEEN
MINNEAPOLIS
ST. PAUL
DULUTH
KEARNEY
HASTINGS
ST. JOSEPH
ATCHISON
TOPEKA
KANSAS CITY
JEFFERSON CITY
HARRISONVILLE
SALINA
SPRINGFIELD
JOPLIN
COFFEYVILLE
BARTLESVILLE
TULSA
ENID
OKLAHOMA CITY
SHAWNEE
NORMAN
ARDMORE
MUSKOGEE
FORT SMITH
LITTLE ROCK
HOT SPRINGS
PINE BLUFF
MEMPHIS
DYERSBURG
UNION CITY
EVANSVILLE
LOUISVILLE
CLARKSVILLE
NASHVILLE
MURFREESBORO
CHATTANOOGA
BIRMINGHAM
BESSEMER
CEDARTOWN
ATLANTA
KNOXVILLE
JOHNSON CITY
BRISTOL
GAINESVILLE
GREENVILLE
WINSTON-SALEM
BURLINGTON
GREENSBORO
RALEIGH
WASHINGTON
SALISBURY
CHARLOTTE
SPARTANBURG
COLUMBIA
GREENWOOD
AUGUSTA
CHARLESTON
DENMARK
SAVANNAH
MACON
AMERICUS
ALBANY
DOTHAN
JACKSONVILLE
TALLAHASSEE
GAINESVILLE
DAYTONA BEACH
TAMPA
WEST PALM BEACH
LAKE WORTH
MIAMI
MOBILE
GULFPORT
NEW ORLEANS
NATCHEZ
McCOMB
MONROE
TEXARKANA
SHREVEPORT
TYLER
LUFKIN
BEAUMONT
HOUSTON
HUNTSVILLE
COLLEGE STATION
PARIS
DALLAS
FORT WORTH
WACO
GRAND PRAIRIE
BROWNWOOD
ABILENE
SAN ANGELO
BIG SPRING
AUSTIN
SAN ANTONIO
LAREDO
CORPUS CHRISTI
HARLINGEN
DENVER
COLORADO SPRINGS
ALBUQUERQUE
ROCK SPRINGS
SALT LAKE CITY
SPOKANE
EVERETT
SEATTLE
PORTLAND
SALEM
OAKLAND
BERKELEY
SAN FRANCISCO
FRESNO
VISALIA
BAKERSFIELD
SAN LUIS OBISPO
LOS ANGELES
RIVERSIDE
BEVERLY HILLS
HOLLYWOOD
SAN DIEGO
LEGEND
○ RECEIVING ONLY POINT
● SENDING AND RECEIVING POINT

Figure 12. A diagram of one of the nationwide multistation circuits for new distribution

Figure 13. Private-line teletypewriter communication center for commercial-airline company

Figure 14. Teletypewriter operating room of a large industrial concern, illustrating arrangements for quickly handling and relaying messages received in perforated-tape form

handled over a network of ring-down interoffice communication channels. These channels are arranged so that an attendant at one office may communicate directly with the attendant at a distant office handling the circuit or facility involved.

As a result of these improvements in stability and continuity, it is now possible to operate nationwide teletypewriter circuits such as that for a press association shown in Figure 12. This service is operated at a speed of 60 words per minute, 24 hours per day, seven days per week.

Large teletypewriter systems involving many individual circuits may require arrangements for readily transferring messages between the various circuits. One method of handling this transfer is by having the messages received in perforated tape form and carrying this tape from the receiver to the transmitter for retransmission over the outgoing circuit. Figures 13 and 14 show two such relay office installations for large industrial businesses.

As an example of recent developments to meet the needs of modern business concerns, a brief description will be given of a fully automatic message switching system (Figures 15 and 16 show two such installations). These concerns have requirements for handling a large volume of short messages between all of the various units of these widespread organizations. Since the distance between the various points is large, it is important to use the intercity facilities efficiently; yet it is important not to have an appreciable delay in the handling of the traffic. To meet these special requirements a fully automatic message switching system was designed. This system is shown diagrammatically in Figure 17 and consists essentially of a number of multistation circuits, some of which are operated on a full-duplex basis, that is, in both directions simultaneously, and others on a half-duplex basis, that is, in one direction at a time. Some operate at 60 words per minute and others at 75 words per minute.

Each station on the system prepares its traffic in the form of a perforated tape. Since there are a number of stations on each circuit, arrangements are provided for automatically starting each station in rotation, the traffic from each station thereby being sent to the automatic switching office. Each message is preceded by certain characters which form a code to control the switching equipment in the switching office, so that each message will be switched automatically across the office, retransmitted on the proper circuit, and received at the designated distant station without attention or handling by operators at the switching or relay office.

Since the messages are short and it is important to keep the long-haul circuits loaded to capacity, the transferring of the messages from one circuit to another at the switching office is accomplished by storing the messages in perforated tape, automatically transmitting them across the office to the proper outgoing line over intraoffice trunks having twice the capacity of the highest-speed intercity line circuit, and then, as the sending circuit becomes available, retransmitting them to the proper destination. Storage is provided at both the incoming and outgoing lines, primarily to permit operating all cross-office circuits at one common speed, so that intercity lines may operate at various speeds as desired. Storage at the incoming line also permits this line to operate continuously without interruption even though cross-office paths may be momentarily busy. A relatively high capacity for accepting and storing messages at the outgoing lines tends to speed up the cross-office handling and keeps storage at the incoming lines to a minimum. It also permits any outgoing line to be shut down temporarily (as required,

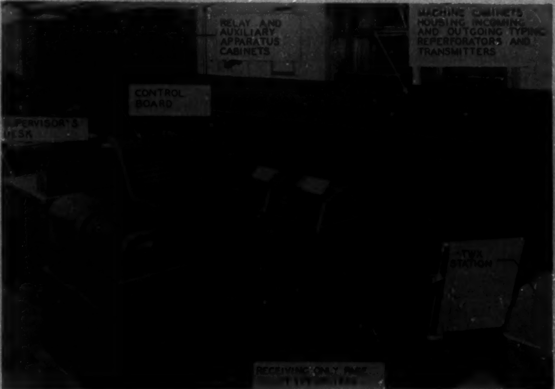

RELAY AND AUXILIARY APPARATUS CABINETS
MACHINE CABINETS HOUSING INCOMING AND OUTGOING TYPING REPERFORATORS AND TRANSMITTERS
CONTROL BOARD
PERVISOR'S ESK
TWX STATION
RECEIVING ONLY PAGE

Figure 15. Portion of teletypewriter operating room of a large industrial concern illustrating arrangements for handling and automatically relaying messages received in tape form

This is believed to be the first installation of equipment for automatically switching messages among a group of circuits

Figure 16. Teletypewriter operating room of a large industrial concern, illustrating a late type of equipment for automatically switching messages

Machines in right foreground are for order-writing purposes, including the preparation of tapes for automatic transmission through other transmitters connected to the switching system

for example, in half-duplex operation), without delaying the progress of other messages in the office.

In the cross-office arrangements two types of intercept circuits are used as shown in Figure 17. One, known as the miscellaneous, serves to intercept and record on a typing reperforator all messages having obviously incorrect directing codes, such as codes not assigned for use in the system. The other, known as willful intercept, may be used for intercepting and recording on a reperforator any message which may require special handling. This is valuable in case a receiving station is closed for certain periods or is temporarily out of order.

One installation employs the typing reperforator–transmitter arrangement previously described. In the normal perforator–transmitter combination, if the perforator stops, a certain amount of traffic is stored between the perforator and the transmitter, since it is necessary to have these two units some inches apart to allow for a loop of tape between them. This necessitates tape feed-out arrangements on the perforator to get this stored message out of the perforator for retransmission in cases where no message immediately follows on the perforator. With this mechanism, however, when the perforator is continuously receiving traffic at a rate faster than it is being transmitted, the transmitter will be a few inches from the perforating point so that a loop of tape may form between the two. Arrangements are provided, so that, if necessary, a large amount of tape may be stored at this point without becoming snarled when it feeds out. If the perforator stops, the transmitter will continue to feed the perforated tape until it becomes taut between the perforator and the transmitter and then will move up towards the perforator until it has sent the last character perforated in the tape. This combination sometimes has been referred to as the "climbing-up-the-tape" transmitter. With this arrangement, no special feed out is required, the transmitting tape will not have blank lengths in it which are necessary when tape feed-out arrangements are used, and considerable circuit time will be saved. Figure 18 shows two typing reperforator–transmitters mounted in a cabinet.

A most useful application of private-line teletypewriter service has been made by various city, county, and state police organizations.[23] Special switchboards have been developed for this service, and the teletypewriter is now a familiar sight in hundreds of police offices. Alarms may be spread over wide areas with great speed and the day-to-day routine exchange of police information can be handled efficiently and speedily.

Figure 19 shows an interesting application of teletypewriters for relaying radio-telephone messages from airplanes to land wires.

Another interesting development is the teletypesetter which provides for automatically setting type from a perforated tape which may be prepared either locally or made automatically from signals received over a telegraph circuit. For example, copy material for a well-known magazine is assembled at New York, edited, punched in tape, and transmitted over telegraph circuits to Philadelphia and Chicago where it is received in perforated tape which is then used for the automatic control of typesetting machines at two separate printing plants. The six-unit selecting code is used, and teletypewriters similar to the number 15, but arranged to accept this code and type both capital and small letters, have been provided to supply page copy for checking purposes.

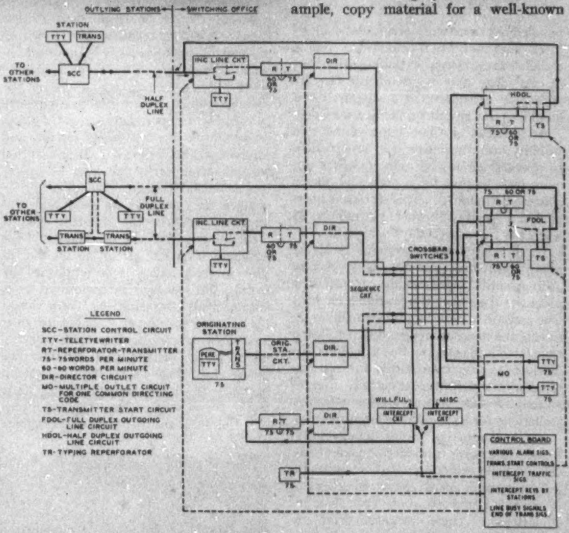

OUTLYING STATIONS
SWITCHING OFFICE
STATION
TTY
TRANS
TO OTHER STATIONS
SCC
HALF DUPLEX LINE
INC LINE CKT
R T
60 OR 75
75
DIR
HDOL
TS
FULL DUPLEX LINE
STATION
FDOL
CROSSBAR SWITCHES
SEQUENCE CKT
LEGEND
SCC–STATION CONTROL CIRCUIT
TTY–TELETYEWRITER
RT–REPERFORATOR-TRANSMITTER
75–75WORDS PER MINUTE
60–60 WORDS PER MINUTE
DIR–DIRECTOR CIRCUIT
MO–MULTIPLE OUTLET CIRCUIT FOR ONE COMMON DIRECTING CODE
TS–TRANSMITTER START CIRCUIT
FDOL–FULL DUPLEX OUTGOING LINE CIRCUIT
HDOL–HALF DUPLEX OUTGOING LINE CIRCUIT
TR–TYPING REPERFORATOR
ORIGINATING STATION
PERF
ORIG. STA. CKT.
MO
WILLFUL
MISC
INTERCEPT CKT
TR
CONTROL BOARD
VARIOUS ALARM SIGS.
TRANS. START CONTROLS
INTERCEPT TRAFFIC SIGS.
INTERCEPT KEYS BY STATIONS
LINE BUSY SIGNALS END OF TRANS SIGS.

Figure 17. Schematic diagram showing functional arrangement of the various elements of a fully automatic message-switching system

Figure 18. Two typing reperforator-transmitters shown mounted in a cabinet with special tape bins and tape winders used in automatic switching office

Figure 19. Radiotelephone and private-line teletypewriter operating positions of a commercial-airline company at a large airport

Teletypewriter Exchange System (TWX)

Teletypewriter exchange service, or *TWX*, is a natural outgrowth of private-line service in which a need had developed for short periods of usage, even less than one hour. Prior to the offering of a public teletypewriter exchange service, considerable experience had been gained with teletypewriter switching, both automatic and manual, for police and other individual private-line customers. There was also, of course, a background of experience in methods and means for the switching of telephone facilities.

The teletypewriter exchange system[16] includes machines installed at convenient locations on subscribers' premises, together with switchboards[36] placed in telephone-company offices and interconnecting circuits, local and intercity. A nationwide directory issued periodically, and including a classified section, lists all of the subscribers with their respective *TWX* numbers. Each nearby *TWX* station is generally connected to a teletypewriter switchboard over a pair of wires. In the case of a remote station, a composited or simplexed grounded circuit usually is used.

By the aforementioned means, instrumentalities are provided for instantaneous to-and-fro typewritten communication between the parties connected. It is a 24-hour service seven days per week, and the calling period for any connection is timed by means of clocks with a three-minute initial period the same as in telephone practice. Since any teletypewriter may be connected at some time to any other teletypewriter in the system, it is necessary that these machines operate at the same speed, and a standard speed slightly over 60 words per minute was selected and made uniform throughout the system. Furthermore, keyboards also must be uniform, and a keyboard having numerals and fractions in the upper case was selected as the standard for *TWX* purposes.

After a *TWX* connection is established, subscribers type back and forth, sending messages and receiving replies. Under these circumstances a sender is certain that his communication has been received, since direct acknowledgment is obtained. If a subscriber desires to receive messages when his station is not attended, special arrangements are provided to permit the switchboard operator to start and stop the station teletypewriter. A noteworthy feature of *TWX* system is the provision of arrangements for furnishing conference connections which include a number of teletypewriter stations, so that each may send and all receive the communications over the network. This is especially useful in making simultaneous announcements or issuing simultaneous orders to a number of points. In *TWX* service the written copy is valuable for record purposes.

The telegraph-exchange idea is old, but it was not until thoroughly reliable teletypewriters and intercity circuits were available that it could become a practical large-scale system.

The *TWX* system has grown to a point where there are today something over 16,000 stations which are connected to 183 switching points with positions for something over 800 *TWX* operators. About 1,300,000 miles of telegraph circuits are employed in this system and approximately 14,000,000 subscriber connections a year are being handled. Figures 20–23 show typical *TWX* switchboard installations.

The general features of *TWX* service have been covered in various papers pre-

Figure 20. Combined inward, outward, and through *TWX* switchboard line-up at New York

Figure 21. Outward *TWX* switchboard line-up at New York

Figure 22. *TWX* and telephone operating room illustrating a number-3*A* *TWX* switchboard installed adjacent to a telephone switchboard

sented to the AIEE,[15,36] but there are certain recent developments which will be described briefly.

AUTOMATIC CONCENTRATING UNITS

Since *TWX* service is offered on a national basis, it becomes necessary to include subscribers who are at remote points. This situation is illustrated in Figure 24 which shows the long loops connecting outlying subscribers to the *TWX* switchboard in Denver. In certain cases there are a number of these remote subscribers grouped in a particular locality where it would not be economical to establish a switching point but possible to concentrate the connections to these stations over trunks. For this purpose there has been developed an automatic concentrating unit which permits an operator at a manual *TWX* switchboard to connect to an idle trunk terminating at the unit and select a particular station at the outlying point by typing the number of that station on her teletypewriter keyboard. The station selected is rung automatically, or, if it is equipped for unattended service, the machine also may be started automatically. A subscriber at the concentrator point originates a call by operating a switch which causes the automatic selection of an idle trunk to the manual office. If all trunks are busy, a special character is received by the calling subscriber's machine. On an inward call, if the subscriber's line is busy, a special character is typed on the *TWX* operator's teletypewriter. After a connection is established, recall and disconnect signals, originated at the station, appear on the associated cord circuit of the manual switchboard. Figure 25 shows schematically the functional arrangement of various elements of this system. The advantage of the unit is that manual work is transferred to a switching point where there are sufficient operators to make possible the savings which result from teamwork and overlapping operations.

SWITCHING EQUIPMENT FOR RAPID INSTALLATION

An interesting development in connection with the engineering of the *TWX* system to meet wartime conditions is the provision of certain plug-connected switchboards which have been introduced into the plant and which are so arranged as to be capable of being quickly disconnected and reinstalled at another point to meet possible emergency conditions. All equipment units are shop wired and fitted with multiple contacts, jacks, and plugs for rapid installation. Only primary power supply, subscriber's lines, and trunk conductors require soldered connections at the time of installation. Figure 26 shows a three-section installation of this plug-connected switchboard.

When the *TWX* system is considered as it is today, the records indicate that, in spite of the handicap of large overloads, it has maintained a high standard of service and has been a real contribution to the wartime communications of the Armed Forces and other government agencies, and to the industrial organizations of the country.

As mentioned earlier, the systems discussed in this presentation provide approximately 4,000,000 miles of telegraph

Figure 23. *TWX* operating room illustrating a small (number 5) *TWX* switchboard for through and terminal business

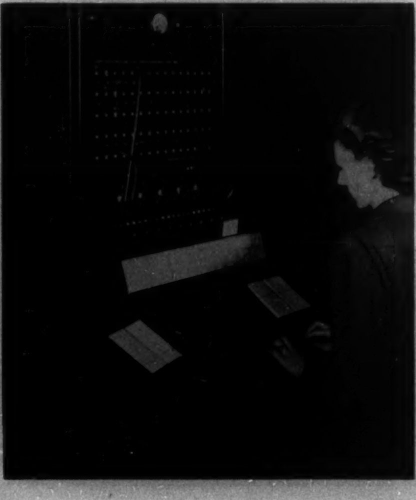
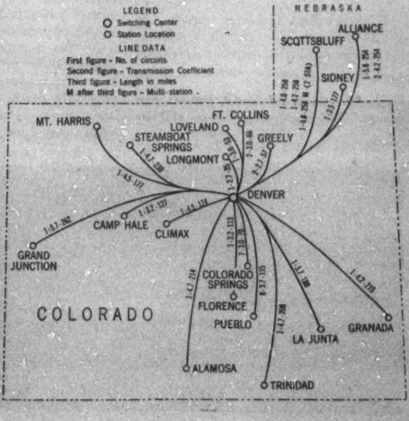


Figure 24. Map illustrating long loops connecting outlying *TWX* subscribers to the Denver switchboard


LEGEND
○ Switching Center
○ Station Location
LINE DATA
First figure - No. of circuits
Second figure - Transmission Coefficient
Third figure - Length in miles
M after third figure - Multi-station
NEBRASKA
ALLIANCE
SCOTTSBLUFF
SIDNEY
1·48·258
1·42·258
1·48·258 M (2 STA)
1·58·254
2·42·254
1·35·177
MT. HARRIS
FT. COLLINS
LOVELAND
STEAMBOAT SPRINGS
GREELY
LONGMONT
1·30·53
2·30·66
2·27·57
1·27·35
1·47·230
1·45·177
DENVER
1·57·262
1·32·127
1·45·174
CAMP HALE
CLIMAX
GRAND JUNCTION
1·32·113
7·30·78
COLORADO SPRINGS
8·37·115
1·47·214
FLORENCE
PUEBLO
1·47·208
1·37·180
1·42·219
COLORADO
LA JUNTA
GRANADA
ALAMOSA
TRINIDAD

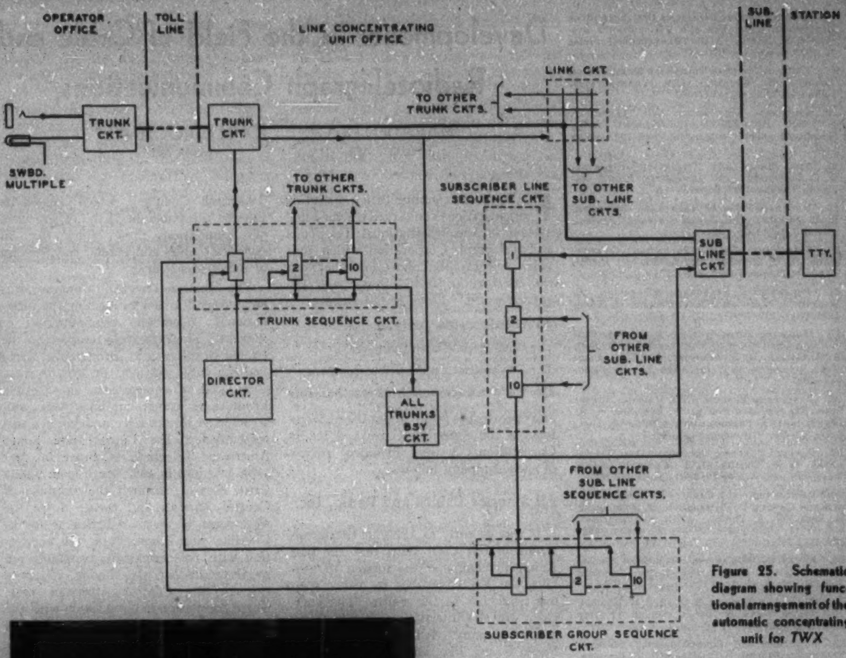

OPERATOR OFFICE
TOLL LINE
LINE CONCENTRATING UNIT OFFICE
SUB. LINE
STATION
LINK CKT
TO OTHER TRUNK CKTS.
TRUNK CKT.
TRUNK CKT.
SWBD. MULTIPLE
TO OTHER TRUNK CKTS.
SUBSCRIBER LINE SEQUENCE CKT.
TO OTHER SUB. LINE CKTS.
SUB LINE CKT.
TTY.
1
2
10
TRUNK SEQUENCE CKT.
FROM OTHER SUB. LINE CKTS.
DIRECTOR CKT.
ALL TRUNKS BSY CKT.
FROM OTHER SUB. LINE SEQUENCE CKTS.
SUBSCRIBER GROUP SEQUENCE CKT.

Figure 25. Schematic diagram showing functional arrangement of the automatic concentrating unit for *TWX*

Figure 26. *TWX* operating room using switchboards arranged for rapid installation to meet emergency conditions

circuit and serve more than 34,000 stations. The use which is made of these facilities by governmental agencies, press associations, industrial concerns, and financial institutions is one measure of their value; it may be said that there is no similar development in any other country that even approaches the private-wire telegraph and teletypewriter exchange systems in America, either in magnitude of the plant involved, in traffic handled, or in over-all service rendered.

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Developments in the Field of Cable and Radiotelegraph Communications

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FOUR record-communication operating organizations constitute the far-flung system of the International Telephone and Telegraph Corporation which provides telegraphic connections between the continental United States and the outside world. These companies are:

All America Cables and Radio, Inc.

The Commercial Cable Company

The Commercial Pacific Cable Company

The Mackay Radio and Telegraph Company

and operate as a group under the management of the American Cable and Radio Corporation. A brief historical review of each company follows.

All America Cables and Radio, Inc.

It was as early as 1865, at the termination of our Civil War, that the first American company was formed to connect the United States by cable with our neighbors to the south. The company was the International Ocean Telegraph Company. It laid a cable between Punta Rassa, Fla., and Havana via Key West, which was first opened to the public in 1866. It is interesting to recall that the first tariff over this short cable was ten dollars for a message of ten words and one dollar for each word in excess.

The leading spirit of this enterprise was James A. Scrymser, who later withdrew from it. In 1878 control of the company was obtained by Jay Gould, and the capital was increased from $1,500,000 to $3,000,000. The Western Union Telegraph Company then leased the International Ocean Telegraph Company for 99 years and guaranteed six per cent on the increased capital.

Although Mr. Scrymser's part in the International Ocean Telegraph Company ceased when its control passed into other hands, his interest in cable communication between the United States and the other American republics was to continue throughout a long and active life. He organized the Mexican Telegraph Company, which was formed in 1878, as well as the Central and South American Telegraph Company, which opened its lines to the public in 1882. These were the foundations upon which the All America Cables and Radio, Inc., system of today was built.

The Mexican Telegraph Company was created with a view to meeting the international telegraph requirements of Mexico. The first cable terminus in the United States was at Brownsville, Tex., and the cable was laid in 1881 to Vera Cruz and Coatzalcoalcos in Mexico. From these points landlines were constructed to Mexico City and Salina Cruz, respectively. The Central and South American Telegraph Company's first cable was laid in 1882 south from Salina Cruz, Mexico, through the republics of Central America and thence down the west coast of South America as far as Chorillos near Lima, Peru. In connection with this extension, two facts are worth recording. In laying its lines through the Central American area, where population in comparison with the large South American cities was small and where the volume of traffic was relatively inconsiderable, the Central American republics were provided with excellent international telegraph facilities at a comparatively early date, and this fact cannot fail to have been a most favorable factor in the development of their foreign trade. The other matter for which the advent of the Central and South American Telegraph Company is responsible is the reduction in rates which the extension of its lines to Lima in 1882 rendered possible. Prior to 1882 the rate between the United States and the Argentine Republic—to give a single example—was $7.50 per word, and the service was by way of Europe. In 1882, via the Central and South American Telegraph Company's lines to Lima and over connecting lines beyond Lima, a rate of $1.85 per word was established between the United States and the Argentine Republic—a most drastic reduction, the advantages of which to commerce in general and the users of telegraph communications in particular are obvious. Among such advantages was the existence of a competitive service between South America and Europe via the United States. Innumerable reductions of the basic tariffs have been introduced since then as well as the cheaper classifications of service now familiar to all.

In 1891, following the termination of a traffic agreement with the West Coast

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of America Telegraph Company, a cable was laid from Chorillos, Peru, to Valparaiso, Chile by way of Iquique, so that thenceforth the companies were able to serve the entire west coast of South America over their own lines. There still remained, however, the need for an independent connection between the United States and the east coast, and to secure this the Transandine Telegraph Company, which operated 1,200 miles of landlines between Chile and Argentina, was purchased in 1891. The addition of this important link made possible the inauguration of a through cable service from Galveston to Buenos Aires and established the foundation for the ultimate extension of the system to Uruguay and Brazil in 1920.

It is not the purpose of this paper to enumerate the various cables laid or the points which they connected.

In 1917 the name of the Central and South American Telegraph Company was changed to All America Cables, Inc.; in 1926 All America Cables, Inc., sold a majority interest in the Mexican Telegraph Company to the Western Union Telegraph Company.

The company's principal radiotelegraph and radiotelephone stations are at Lima, Peru, and Bogota, Colombia. Smaller radiotelegraph stations are operated at other points in Central and South America. It owns or operates under lease 29,208 nautical miles of submarine or subfluvial cable and 3,577 statute miles of telegraph landlines in connection therewith. It maintains one or more offices at 68 different points in 24 countries or islands of the Western Hemisphere.

In 1927 All America Cables, Inc., became an associated company of International Telephone and Telegraph Corporation, and in 1938 the name was changed to All America Cables and Radio, Inc.

The Commercial Cable and Commercial Pacific Cable Companies

In the beginning of the 1880's, the telegraph facilities between the United States and Europe were preponderantly foreign and included the cables of the Anglo American Telegraph Company, Ltd., the Direct United States Telegraph Company, Ltd., both British, and those of the French Cable Company. The American Telegraph and Cable Company had two cables leased by Western Union Telegraph Company. All of these companies had entered into an agreement for pooling their receipts and fixing rates.

In order to break this foreign monopoly of communications and to enable American interests to benefit from more reasonable rates, John W. Mackay and James Gordon Bennett entered into a partnership which resulted in the formation of The Commercial Cable Company and the laying of its first two cables in 1884 between Canso, N. S., Canada and Waterville, Ireland, with extensions to the United States on one side and to England and France on the other side. The reduced rates offered by The Commercial Cable Company resulted in a prolonged-rate war between that company and the foreign-controlled pool, which theretofore had maintained a charge of 50 cents per word across the Atlantic ocean. Despite the pressure brought to bear on it to join the existing pool and to maintain the higher rates, The Commercial Cable Company insisted on applying lower rates with the result that 25 cents a word was finally adopted by all companies.

In 1894 the third main cable was laid across the Atlantic, and subsequently three other transatlantic cables were added to The Commercial Cable Company's system, together with a number of coastal cables on the American seaboard and on the European side. These provided an adequate network which gave the company not only an efficient outlet for its cables on both sides of the Atlantic, but also provided alternate routes in case of interruption.

During the first years of this century, The Commercial Cable Company moved to the Pacific. The Commercial Pacific Cable Company was formed, and, after formidable difficulties had been overcome, a cable was laid from San Francisco to Honolulu in 1902 and thence to Midway Island, Guam, and Manila, with extensions to China and Japan. Some idea of the physical obstacles encountered may be formed from the fact that not only was the cable longer than any previously laid (for example, the distance between San Francisco and Shanghai is in the neighborhood of 9,000 miles), but also, greater depths, reaching over 5,000 fathoms, or more than $5^1/_2$ miles, were met than any in which cables had ever been laid. The cable has been raised successfully for repairs from a depth of over 4,000 fathoms. With the completion of the Shanghai extension in 1906, telegraphic encirclement of the globe was achieved. On July 4 of that year, President Theodore Roosevelt and Clarence H. Mackay, president of The Commercial Cable Company, exchanged the first telegraph messages that ever traveled entirely around the world. The message from President Roosevelt was delivered to Mr. Mackay nine minutes after it was filed.

The inauguration of service over the Pacific cable resulted in immediate rate reductions between San Francisco and China from $1.72 per word via the Atlantic to $1.10 via Commerical Pacific Cable Company; the Philippines from $2.47 to $1.00 per word; and Japan from $1.88 to $1.21 per word. Subsequently, these have undergone further substantial reductions.

In the following years, The Commercial Cable Company expanded its communication network in the Atlantic to 23,177 miles and in the Pacific to 10,068 miles—a total of 33,245 miles of submarine cables. It established its own operating stations in various strategic points providing direct connections between New York and London, Paris, Antwerp, Brussels, Rotterdam and other cities. It has, at present, one or more cable offices in 16 of the most important points in Great Britain and Ireland.

In 1928 The Commercial Cable Company became an associated company of International Telephone and Telegraph Corporation.

Technical Developments in the Operation of Cables

Submarine cables always have been very expensive ventures, and great hazards were involved in the beginning, when the difficulties were still unknown. After a few cables had been laid the conditions and risks limiting them began to be known, but the general engineering in connection with the first long cables was so sound that their type since has been altered very little. The early technique of operating cables was in the hands of a small group of men who were not engineers nor scientists, but who merely tried hard to make the most of what was available.

In 1858 Sir William Thomson introduced the first means of receiving the very weak signals arriving over long cables using a mirror instrument, which was a form of reflecting galvanometer, and the signals were read from the movements of the spot of light. Sir William Thomson also made the first improvement on this method of reception when he invented the siphon recorder, in 1867, which recorded the signals on a strip of paper in the form of a wavy line of ink. This instrument gradually supplanted the mirror on all long cable sections, so that when the first cables of the All America Company system were laid in 1881, the siphon recorder naturally was installed as the receiving instrument.

To prevent friction on the recorder paper, the siphon was maintained a short distance from the surface of the paper, and the ink was electrified, which caused it to spurt in dots onto the paper. A great improvement was made in 1884, when one of the All America Company staff, G. F. Pescod, devised a method of mechanically vibrating the siphon giving a fine dotted line to the record and still overcoming the paper friction. This system of mechanical vibration soon was adopted by every "recorder" telegraph station throughout the world.

As the All America Company system to South America was made up of a long series of cables forming a chain, linking the cables by relay obviously was necessary. Successive stages of development over the years has brought about the present-day fully automatic-synchronous-regenerated relay chain.

The first method employed the human relay, where the operator at the first station sent his message by hand key. At the other end of the cable the receiving operator received the message from the tape, and it was retransmitted by hand. This process was repeated until the message reached its destination. With the "human-relay" chain, the transmission time factor was good; however, there were the limitations in the rate of sending and the human element which introduced errors. Development was concentrated in efforts to overcome these two adverse elements.

To increase the speed of sending messages, the use of automatic cable transmitters was adopted as these became available on the market. In the early days, the paper transmitting tape was perforated by the hand-operated "stick" punch, which imposed a limit on the transmitting speed per operator, as each signaling hole in the tape had to be punched individually. This limit was raised by the keyboard perforator, which punched a complete letter group of holes with one depression of a key on a keyboard similar to that of a typewriter.

The speed problem also depended on conditions at the receiving end. The amplitude of the incoming signal depends on the electrical constants of the cable and so is a fixed quantity for given sending conditions. To care for the smaller amplitudes associated with the higher sending speeds, magnifiers were used as they became available. The first of these, that was experimented with, were light-sensitive cells, but subsequently the familiar Heurtley magnifier was adopted which depends for its action on the change of resistance with temperature of a very fine platinum wire. As might be expected, electronic-valve methods constitute the ultimate in magnifiers, but these, as yet, have not been adopted by this system.

Since the start of the system in 1881, these cables have been duplexed, permitting simultaneous operation in both directions, and every speed increase has called for improvements in the "balance" involved in the proper adjustment of the special circuits used. This is where long experience and individual skill give results. The ability to obtain a good balance is still largely a trial-and-error empirical problem.

Progress on the problem of reducing the human element paralleled the efforts to increase the speed of transmission. The first thing to be done was to obtain a cable relay that was reliable, and this was possible only after automatic transmission had been installed at all sending stations. The Muirhead gold-wire relay proved the most suitable after it had been changed somewhat from the original design. First there were single relays between two cables, and then it was possible to extend this, linking two and finally three cables in tandem to form a chain.

Each relay, however, retained and passed along some of the distortion accumulated on previous links. To overcome this handicap we developed an automatic reperforator to punch a new transmitting tape automatically from the relayed signals. This gave the means of setting up complete chains from station of origin to station of destination without any human retransmission, but at the sacrifice of introducing a small time lag at each reperforating point. This step forward emphasized the disadvantages of the variability of the transmitter speed which imposed a limit to the stability of working. This problem was solved by making use of tuning forks to control the speed of the automatic transmitter.

With the advent of the tuning-fork speed control came another possibility, which was taken advantage of by the development of the synchronous regenerator, whereby a new signal could be regenerated and sent directly into the next cable section as a perfect signal instead of punching a whole new tape. Thus, the accumulated distortion of the relays was eliminated, as well as the time lag due to punching a new tape.

Thus was evolved the achievement of sending messages automatically from one end of the system to the other without human intervention at intermediate points. To take care of branching traffic, it is possible to reperforate automatically a new transmitting tape at the appropriate point. The human-error factor thus was reduced to the original sender and the ultimate receiver. Of these two, only the human receiver could be eliminated. This was done by the development of an automatic printer method which takes the signals coming from the cable and automatically records them as regular printed characters on a continuous paper tape, or preferably as printed characters in page form ready for delivery to the customer.

While landline telegraphs were developing carrier transmission, this principle was not neglected by the cable companies, and a form of higher-frequency transmission was superimposed on short cables in order to make use of the extra carrying capacity which they had in excess of that of the long cables with which they were linked.

Because of advances in methods of operation on the old cables and to basic cable design which permitted higher speeds on some of the new cables, it was found possible in some cases to secure transmission speeds in excess of the capacity of one operator to transmit or to receive, and, therefore, methods for making available more than one channel per cable were introduced and adopted.

An interesting phase of long automatic chains has been the development of "selectors," whereby the sending station can call the attention of any desired station to a particular circuit or automatically can start up an automatic reperforator and punch a new tape at any required point.

While radiotelegraphy over long distances was making use of long waves (10,000 to 30,000 meters—a system which involved very expensive terminal installations at each end), the cable companies showed very little interest in adopting radio methods, although The Commercial Cable Company in 1921 acquired control of the Radio Communications Company, Inc. When short-wave transmission was demonstrated as practical, the Mackay Company, at that time the parent company of The Commercial Cable Company, acquired the communication system of the Federal Telegraph Company of California, which, with the Radio Communications Company referred to previously, led to the formation of the Mackay Radio and Telegraph Company. Meanwhile, All America Cables, Inc. acquired radio concessions in various countries and installed at first experimental and later commercial radio stations to parallel or supplement their cable links.

The Mackay Radio and Telegraph Company

The Federal Telegraph Company commenced activities in California about 1909, having been organized by a group of Stanford University men who had secured rights to the patents of Poulsen and Pedersen of Copenhagen, Denmark. At that time the only workable method of radio communication was by the use of damped waves generated by the spark type of equipment. This method was inherently inefficient, and the power available at the transmitter could be utilized only partially. Practical application was confined chiefly to communication with ships at sea. The use of radio for point-to-point telegraph was very limited, mainly because of the inability to cover long distances reliably, particularly in the daytime, such reliability being necessary to compete with other established communication systems.

The Danish inventors had developed a type of high-frequency generator employing an electric arc in an atmosphere of hydrogen which made possible the first successful method of communication with sustained or undamped waves. This gave the Federal Telegraph Company a tremendous advantage over others, because this method enabled all the available power to be concentrated efficiently in a single wave and also permitted the full effective use of newly discovered methods of reception which were not particularly applicable to the damped-wave spark type of system. Although some of the advantages of sustained-wave transmission were known prior to the advent of the Poulsen arc, there was no way of using that method for commercial systems, because the only instrumentalities available for generating the required currents were incapable of developing any appreciable power. The Poulsen arc, however, provided a means of generating plenty of power with a relatively simple apparatus not involving intricate nor unstable features.

The Federal Telegraph Company promptly established commercial radiotelegraph services between San Francisco, Los Angeles, San Diego, and Portland, Oreg., in 1911 and between San Francisco and Honolulu in 1912. Wave lengths of the order of 3,000 to 10,000 meters were used, and competition began for business with the existing landline telegraph and cable companies at lower rates. This business grew to such a point in 1915, that additional facilities were added for the purpose of providing duplex operation, and extra transmitting and receiving stations at San Francisco and Los Angeles were created to increase the number of channels. The immediate success of the long-distance transocean circuit between San Francisco and Honolulu in 1912 encouraged the Federal Telegraph Company and the Navy Department to install a Federal Telegraph Company arc transmitter at the Navy Department's radio station at Arlington, Va., in 1912. As a result of this, the Arlington station was able to communicate with the San Francisco and Honolulu stations during daylight hours, a feat never before accomplished. The success of these and other trials caused the Navy Department to adopt the Federal Telegraph Company arc system as standard for its service. An extensive construction program was started in 1913, which covered a chain of high-power Navy radio stations, connecting Washington, D. C., with the Canal Zone, California, Hawaii, the Philippines, and Puerto Rico, ranging in power from 100–500 kw, together with a secondary system of medium-power equipments at all important naval establishments on United States territory and on ships of the fleet. The climax to this development was reached with the construction of the large radio station built near Bordeaux, France, during World War I for which the Federal Telegraph Company supplied two transmitters of 1,000 kw each.

In 1914 the Federal Telegraph Company entered the marine radio field at San Francisco, which enabled ships plying the Pacific to secure daylight communication over tremendous distances that theretofore had been impossible.

Following World War I, in 1921, the company constructed a new communication system for itself along the Pacific Coast, equipped for three complete duplex telegraph channels between San Francisco and Portland and three between San Francisco and Los Angeles, together with other innovations, including remote receiving stations. This enabled terminals for both sending and receiving messages to be set up and operated in downtown business offices in the respective cities, which eliminated the time and expense of handling messages between these city centers and the radio stations which had to be located on the outskirts. An example of such direct handling was the service given for many years between the floors of the San Francisco and Los Angeles stock exchanges where traders could get buy-and-sell order messages delivered in a matter of seconds.

This point-to-point service continued with the original domestic-rate schedules established in 1911, which were similar to those offered by the land-wire systems, except that 15 words were permitted as a minimum instead of the usual 10, and 60 words instead of 50 for day and night letters. This general rate schedule continued until domestic radiotelegraph operations were discontinued in 1942 because of the war.

As has been stated, Mackay Radio and Telegraph Company acquired the system of the Federal Telegraph Company in 1927. Not long after that it also purchased the entire Federal Telegraph Company, thereby securing its manufacturing facilities and other assets, which today have grown to become the Federal Telephone and Radio Corporation with factories located in New Jersey and development and research laboratories in New York City.

Since 1927 Mackay Radio Company, with Federal Telegraph Company as its manufacturer, embarked on an extensive reconstruction program coupled with new construction employing the newly found technique of short-wave transmission and expanded its service to many other American cities and internationally to important points in Europe, Africa, Central and South America, New Zealand, Australia, and the Orient, so that it now directly serves 28 points beyond the continental United States. It also has built up an extensive ship-to-shore radiotelegraph service having seven radio stations along the United States coasts and service contracts covering radio facilities on 1,100 American vessels, with service depots in 17 United States ports, and world-wide service arrangements with associated companies.

Improved technical means have progressed as this communication system has developed, making possible higher transmission speeds, more effective directive antennas, and refinements in apparatus details made available through advances in the radio sciences. Automatic-relay methods for connecting radiotelegraph circuits together also have been employed regularly. Transmission speeds as high as 500 words per minute have been employed successfully over long-distance circuits. A service for the transmission of pictures and facsimile material has been provided. With the advent of very high-frequency wave generation and detection technique in recent years, radio-control facilities, employing these very short waves, have been established between the radio-terminal offices in city centers and the outlying radio-transmitting and receiving stations associated with them, which radio connections are now used to a large extent in place of wire lines.

Analysis of Rectifier Circuits

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THE general principles employed in the analysis of rectifier circuits have been quite fully covered in the literature and are now well understood. However, there is not available a complete analysis giving the essential formulas in a systematic and unified form readily usable by the engineer. It is the purpose of this paper to present such a treatment.

A large variety of rectifier circuits has been devised. The analysis of many of the different types of circuits and the determination of their operating characteristics are greatly facilitated by the circuit simplifications embodied in the concepts of

1. A rectifying element.
2. Simple, multiple, and cascade rectifiers.
3. Single-way and double-way rectifiers.
4. The reactance factor.[1]

By the application of these ideas a standardized procedure for the analysis of rectifier circuits has been developed. This method is illustrated by the analysis.

In the interest of brevity many parts of the analysis are presented by a statement of the principles which apply and the expressions which may be derived. In such cases reference is made to standard texts for detailed derivation of formulas, discussion of principle, and validity of assumption.

General Considerations

Procedure. The analysis is divided into three parts in order to simplify the mathematical treatment and show the action more clearly. Wave forms for all three cases are shown on Figure 1.

Case I shows the rectifier action with no overlap. The effect of transformer reactance is neglected, and it is assumed that the current transfer from anode to anode takes place instantaneously. The anode current waves are assumed rectangular in form, and the wave forms in this case are generally referred to as the theoretical wave forms.

Case II shows the action with overlap and introduces the effect of transformer reactance in modifying the relations developed in case I.

Case III shows the effect of both phase control and overlap upon the rectifier action and characteristics.

The analysis is carried through using the delta–double-wye circuit as an example, as this circuit is more widely used and better understood than any other. Also, its analysis involves all of the features found in most rectifier circuits.

Mode of Operation. In general, several modes of operation are possible for any rectifier circuit, the various modes differing in the shape of the anode current waves. The mode of operation is determined by the constants of the a-c and d-c circuits. For example, if the d-c circuit is noninductive and the rectifier supplies a load with a high counter electromotive force or operates with a large amount of phase control, the load current may be discontinuous. Under these conditions the anode current wave may be either round-topped or approximately triangular in form. If the d-c circuit is highly inductive the load current is constant and the anode current waves are flat topped. Again, under short-circuit conditions, each anode may carry current for approximately 180 degrees with a current wave which is approximately sinusoidal. These are some of the more common modes of operation.

In order to facilitate analysis it is generally assumed that the d-c load supplied by the rectifier is highly inductive. This condition is usually quite closely approximated in service and will be called the normal mode of operation.

Assumptions. In this analysis it is assumed that the rectifier is operating in the normal mode with a highly inductive d-c load except where specifically stated otherwise. The a-c circuit is assumed balanced with the a-c phase voltages equal in magnitude, 120 degrees apart in phase, and sinusoidal in form. The interphase-transformer exciting current is assumed negligible. Further assumptions will be noted as they arise.

A list of rectifier symbols is included in the appendix.

Analysis of Theoretical Case

DIRECT VOLTAGE

The delta–double-wye is a multiple single-way rectifier circuit, consisting of two simple rectifiers, each having three rectifying elements (or anodes). Three of the rectifying elements and the odd-numbered transformer secondary windings to which they are connected form one simple rectifier, while the other three rectifying elements together with the windings of the even-numbered wye form another simple rectifier. These two simple rectifiers have the same average direct-voltage output and operate in multiple on a common output circuit. However, their instantaneous direct voltages are unequal, as the voltages of the various windings are out of phase. The interphase transformer permits parallel operation as it furnishes the necessary equalizing voltage.

A graphic analysis such as that in Figure 1 is often helpful in studies of the circuit action and may be readily constructed as follows: Starting with the transformer secondary line-to-neutral voltages, the direct voltage of anode 1 is shown by the segment of the secondary-voltage curve bounded by the shaded area. Each rectifying element conducts current for 120 degrees and the output voltage of the simple rectifier comprising anodes 1, 3, and 5 consists of three such voltage waves per cycle. The direct-voltage wave of anodes 2, 4, and 6 is similar in form but displaced 60 degrees in phase. The direct voltage of the multiple rectifier at any instant is equal to the average of the voltages of the two simple rectifiers at that instant, and its instantaneous direct-voltage wave is obtained by plotting the locus of the instantaneous average values throughout the cycle. The average direct voltage is a line drawn through the mean of these average values.

The general equation for the average direct voltage of a simple rectifier having any number of phases is readily derived from the voltage wave of a single rectifying element by integration.[2] Referring to Figure 2, the average value of the direct voltage of anode 1 is

$$E_{d_0}=\frac{p}{2\pi}\int_{-\pi/p}^{+\pi/p}\sqrt{2}E_s\cos\theta d\theta \qquad (1)$$

Performing the integration the general equation is obtained:

$$E_{d_0}=\sqrt{2}\,E_s\frac{p}{\pi}\sin\frac{\pi}{p} \qquad (2)$$

where

E_{d_0} = theoretical direct voltage (average direct voltage assuming no overlap, no phase control, and zero arc drop)
E_s = rms value of transformer-secondary line-to-neutral voltage
p = number of secondary phases in the simple rectifier

For the delta–double-wye circuit in the normal mode of operation $p=3$ and

$$E_{d_0}=\frac{3\sqrt{3}}{\pi\sqrt{2}}E_s=1.17E_s \qquad (3)$$

Normally the delta–double-wye circuit operates as two simple rectifiers in multiple with the interphase transformer excited. At light load when the load cur-

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rent is not sufficient to excite the interphase transformer, the circuit operates as one simple rectifier with $p=6$ and a higher value of direct voltage is obtained as will be found by substituting in equation 2. The rise in voltage at light load which is characteristic of many rectifier circuits is due to a change in mode of operation of this nature.

The voltage E_{do} enters directly in many of the mathematical expressions applying to the circuit action and for convenience will be referred to hereafter as the theoretical direct voltage. This voltage is used as a base for calculating per unit voltages inasmuch as its use permits considerable mathematical simplification.

Ripple in Direct Voltage. The load current is carried by the various anodes in succession, the anodes, one in each simple rectifier, having the highest positive voltage carrying the current at any instant. The direct-voltage wave is, therefore, formed by a succession of segments of positive sine waves and has a ripple whose form is repeated six times during each cycle of alternating supply voltage as shown in Figure 3. The magnitude and character of this ripple may be described in a variety of ways,[3] the choice of which depends upon the use to be made of the information.

The rms value of the ripple voltage may be calculated directly by integration of the difference between instantaneous and average values of the direct-voltage wave. In the theoretical case, where q is the number of phases of an equivalent simple rectifier,

Rms ripple voltage =

$$E_{do}\sqrt{\frac{q}{\pi}\int_0^{\frac{\pi}{q}}\left[\left(\frac{\pi}{q\sin\frac{\pi}{q}}\right)\cos\theta-1\right]^2 d\theta} \qquad (4)$$

In the delta-double-wye rectifier $q=6$, and

$$\text{Rms ripple voltage}=0.0418E_{do} \qquad (5)$$

The average value of the ripple voltage may be calculated in a similar manner. The rms and average values of the ripple voltage may be measured directly by means of suitable a-c voltmeters connected across the smoothing reactor in the rectifier output circuit.

The ripple voltage may also be resolved into its harmonic components.[4] An analysis of the theoretical direct-voltage wave shows that, besides the d-c component, it contains harmonic components the frequencies of which are mq times the a-c system frequency. The theoretical direct voltage may be represented by the Fourier series:

$$e=E_{do}+b_1\cos q\theta+b_2\cos 2q\theta+b_3\cos 3q\theta\ldots+ b_m\cos mq\theta+\ldots \qquad (6)$$

where

e = instantaneous direct voltage
E_{do} = theoretical direct voltage
b_1, b_2, etc. = amplitude of harmonic components
q = number of phases of the equivalent simple rectifier having the same wave shape
m = 1, 2, 3, etc., order of harmonics
θ = angle measured from crest of voltage wave, see Figure 3

The amplitude of any one of the harmonics is

$$b_m=\frac{2}{m^2q^2-1}\cdot E_{do} \qquad (7)$$

The telephone influence factor[5] is a

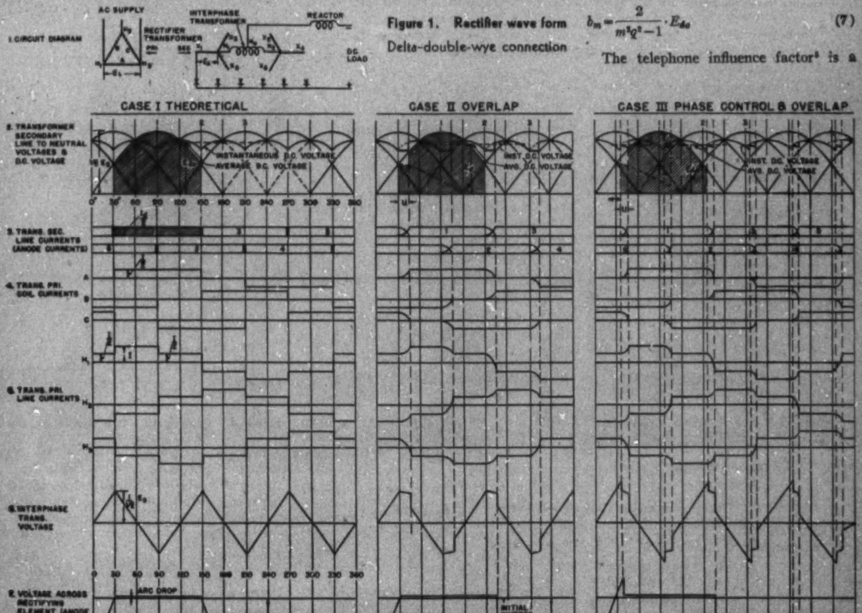

1. CIRCUIT DIAGRAM
AC SUPPLY
RECTIFIER TRANSFORMER
PRI
SEC
INTERPHASE TRANSFORMER
REACTOR
DC LOAD
CASE I THEORETICAL
CASE II OVERLAP
CASE III PHASE CONTROL & OVERLAP
2. TRANSFORMER SECONDARY LINE TO NEUTRAL VOLTAGES & D.C. VOLTAGE
INSTANTANEOUS D.C. VOLTAGE
AVERAGE D.C. VOLTAGE
INST. D.C. VOLTAGE
AVG. D.C. VOLTAGE
0° 30° 60 90 120 150 180 210 240 270 300 330 360
3. TRANS. SEC. LINE CURRENTS (ANODE CURRENTS)
4. TRANS. PRI. COIL CURRENTS
A
B
C
5. TRANS. PRI. LINE CURRENTS
6. INTERPHASE TRANS. VOLTAGE
7. VOLTAGE ACROSS RECTIFYING ELEMENT (ANODE TO CATHODE VOLTAGE)
ARC DROP
PEAK INVERSE
INITIAL INVERSE

Figure 1. Rectifier wave form

Delta-double-wye connection

further measure of the magnitude of the ripple in the direct output voltage. This factor provides a convenient over-all index of the influence of a complex voltage or current wave on noise induction in telephone circuits. In the calculation of the telephone influence factor each harmonic is weighted in accordance with a standard frequency-weighting characteristic. The telephone influence factor for the theoretical direct-voltage wave of a delta-double-wye rectifier has a value of 63 with weighting factors per Edison Electric Institute "Report on Rectifier Wave Shape," publication $E1$, April 1937.

ALTERNATING CURRENTS

In the theoretical case each rectifying element of the delta-double-wye circuit conducts current for 120 degrees. The currents in each of the rectifying elements (anodes) and the transformer secondary windings are as shown in Figure 1. As the load current, I_d, is shared equally by the anodes, the average anode current is

$$\text{Average anode current} = \frac{I_d}{6} = 0.167 I_d \tag{8}$$

The rms value of the transformer secondary-coil current and the primary coil and line currents may be calculated readily from the current waves by integration using the general formula

$$\text{Rms current} = \sqrt{\frac{1}{T}\int_0 i^2 dt} \tag{9}$$

where

i = the instantaneous current
t = time
T = interval of integration

The rms anode current, I_s, in the delta-double-wye circuit is

$$I_s = \frac{I_d}{2\sqrt{3}} = 0.289 I_d \tag{10}$$

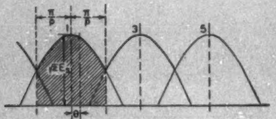


Figure 2. Direct-voltage wave of a simple rectifier

In order to satisfy the magnetomotive force relations for transformer operation there can be no unidirectional component of primary current, and the instantaneous primary and secondary ampere turns on any leg of the transformer must be equal and opposite (neglecting magnetizing current). Applying this rule, the wave form of the transformer primary-coil currents may be determined graphically as shown in Figure 1. The transformer primary-coil current in each phase is equal to the sum of the currents in the secondary coils which lay in the same phase, making proper correction for turn ratio. The rms primary-coil current I_p is

$$I_p = \frac{I_d}{\sqrt{6}} \cdot \frac{E_s}{E_L} = 0.408 I_d \cdot \frac{E_s}{E_L} \tag{11}$$

where

E_s = transformer secondary line-to-neutral voltage in rms volts
E_L = transformer primary line-to-line voltage in rms volts
I_d = d-c load current in average amperes
E_s/E_L = turn ratio

The instantaneous primary line current at any primary terminal is equal to the sum of the currents in the two primary coils connected to that terminal, and the rms primary line current, I_L, is

$$I_L = \frac{I_d}{\sqrt{2}} \cdot \frac{E_s}{E_L} = 0.707 I_d \cdot \frac{E_s}{E_L} \tag{12}$$

HARMONICS IN ALTERNATING LINE CURRENT

If the theoretical primary line current of the delta-double-wye rectifier is analyzed for its harmonic components,[6] it is found that it may be represented by the following Fourier series:

$$i = \frac{3I_d}{\pi}\left(\frac{E_s}{E_L}\right)\left[\sin\theta + \frac{1}{5}\sin 5\theta + \frac{1}{7}\sin 7\theta + \frac{1}{11}\sin 11\theta + \frac{1}{13}\sin 13\theta \ldots \frac{1}{m}\sin m\theta \ldots\right] \tag{13}$$

where

i = instantaneous primary line current
I_d = average load current
E_s/E_L = turn ratio
$m = nq \pm 1, 5, 7$, etc.
θ = angle measured from beginning of positive current wave

The telephone influence factor for the theoretical primary line current has a value of 975.

PRIMARY AND SECONDARY VOLT-AMPERES

From the foregoing relations between output and input voltages and currents, the volt-ampere loadings of the transformer windings can be readily determined in terms of the d-c power output of the rectifier.[7] The primary volt-amperes are

$$3E_L I_p = \frac{\pi}{3} E_{do} I_d = 1.047 E_{do} I_d \tag{14}$$

and the secondary volt-amperes are

$$6E_s I_s = \sqrt{2}\frac{\pi}{3} E_{do} I_d = 1.48 E_{do} I_d \tag{15}$$

In the delta-double-wye circuit the primary volt-amperes and the line volt-amperes are equal. However, where two rectifier circuits differing in phase operate in parallel from the same a-c line, as for example, a delta-double-wye and a wye-double-wye, the total line volt-amperes will not equal the sum of the separate line volt-amperes for the two circuits. In such cases the total line volt-amperes will be less than the sum of the separate line volt-amperes because of the improvement in wave shape obtained by the phase displacement.

POWER FACTOR

The rectifier power factor is defined as the ratio of the power input to the input volt-amperes.[8] Assuming zero transformer magnetizing current, and substituting the relations already determined between output and input and currents, the power factor in the theoretical case is

$$Pf = \frac{3}{\pi} = 0.955 \tag{16}$$

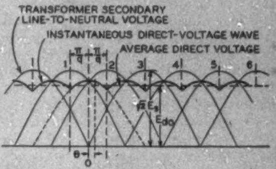
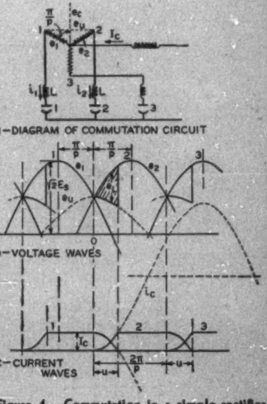


Figure 3. Direct-voltage wave of a delta-double-wye rectifier



a—DIAGRAM OF COMMUTATION CIRCUIT

b—VOLTAGE WAVES

c—CURRENT WAVES

Figure 4. Commutation in a simple rectifier

INTERPHASE-TRANSFORMER VOLTAGE

The voltage which appears across the interphase transformer may be readily determined from the transformer-secondary line-to-neutral voltages, since its instantaneous value is equal to the difference in output voltage of the two simple rectifiers. It is nearly triangular in form and has a fundamental component three times the supply-line frequency. The rms voltage across the interphase transformer in the theoretical case is

Rms interphase voltage

$$=\sqrt{\frac{6}{\pi}\int_0^{\frac{\pi}{6}}[\sqrt{2}E_s \sin\theta]^2 d\theta}=0.415E_s \quad (17)$$

The flux density in the case of the interphase transformer depends upon the average voltage rather than the rms voltage. The average voltage across the interphase transformer is

Average interphase transformer voltage

$$=\frac{6}{\pi}\int_0^{\frac{\pi}{6}}\sqrt{2}E_s \sin\theta d\theta=0.361E_s \quad (18)$$

VOLTAGE ACROSS RECTIFYING ELEMENT

The duty on the rectifying element arises from both the passage of current during the conducting period and the application of inverse voltage during the idle period. It is desired therefore to know the form and magnitude of the inverse-voltage wave. The inverse voltage on an anode at any instant is equal to the voltage between it and whichever other anode is conducting current at that instant. The inverse-voltage wave form is readily constructed from the secondary-voltage waves as shown in Figure 1. The value of the peak inverse voltage e_{pi} is

$$e_{pi}=\sqrt{6}E_s \quad (19)$$

$$=\frac{2\pi}{3}E_{do}=2.094E_{do} \quad (20)$$

Analysis With Overlap

The reactance of the rectifier transformer, a-c supply system, and other a-c circuit elements prevents the instantaneous transfer of current from anode to anode in a rectifier. Due to the presence of this reactance, a definite time interval is required for commutation. The length of this interval is usually measured in electrical degrees and is referred to as the angle of overlap u.

The voltage and current wave forms of the rectifier are modified by the overlap, as are also the formulas which apply. The wave forms with overlap are shown on Figure 1 under case II. The overlapping of the anode firing periods results in a reduction in the rectifier output voltage and determines the regulation, power factor, harmonics in a-c input and d-c output wave forms, and many other characteristics.

COMMUTATING VOLTAGE

In a simple rectifier without phase control, commutation begins at the instant that the voltages on the anode which is firing and on the anode which fires next in order become equal. (See Figure 4.) Choosing this time as a reference point, the voltage difference between the two anodes during the commutation interval which follows (that is, the commutating voltage e_c) is

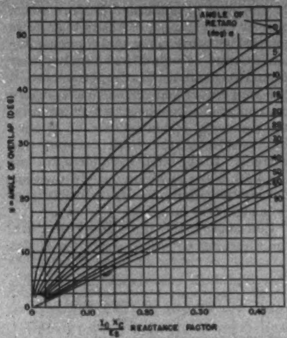


Figure 5. Angle of overlap—characteristic curves

$$p=3$$
$$\cos(u+\alpha)=\cos\alpha-0.816\frac{I_c X_c}{E_s}$$

$$e_c=2\sqrt{2}E_s \sin\frac{\pi}{p}\sin\omega t \quad (21)$$

where

E_s = rms value of transformer secondary line-to-neutral voltage

p = the number of phases in each simple rectifier

$\omega = 2\pi f$

f = frequency of a-c system

In the delta-double-wye circuit $p=3$ and the commutating voltage is therefore

$$e_c=\sqrt{3}\sqrt{2}E_s \sin\omega t \quad (22)$$

COMMUTATING REACTANCE

The commutating reactance is the reactance which effectively opposes normal current transfer between anodes which conduct consecutively. The simplest case is obtained when all the reactance is located in the leads between the transformer secondary windings and the rectifier anodes. In this case the commutating reactance (or line-to-neutral commutating reactance, as it is sometimes termed) is the reactance in series with one secondary phase. This is the reactance which appears in the formulas which follow.

The complete commutating circuit includes all the reactance from line to line (anode to anode). In the simple case where all the reactance is located in the leads between the transformer secondary windings and the rectifier anodes, the line-to-line commutating reactance is equal to twice the line-to-neutral commutating reactance. Measurements or calculations of commutating reactance made on both line-to-line and line-to-neutral basis may show other than a two-to-one relationship between the two values in cases where the reactance is located in the transformer windings and there is mutual coupling between the two secondary windings involved. Inasmuch as the current transfer is opposed by the reactance from line to line, measurements and calculations are made on this basis. The value of the line-to-neutral commutating reactance X_c is then assumed to be one half of the line-to-line value.

In the actual rectifier circuit the reactance is usually distributed in various parts of the circuit, such as the transformer secondary windings, primary windings, and the a-c supply system. For purposes of calculation these reactances usually may all be lumped in the secondary by employing suitable transformations to determine the equivalent commutating reactance due to transformer primary and a-c supply reactances.

COMMUTATION

Expressions for the anode currents during the commutation period may be derived by drawing the diagram of the commutation circuit, applying Kirchhoff's laws to obtain the relations between the voltages and currents, and then solving the resulting equations.[9]

The commutation circuit and the voltage and current relations during commutation of a simple rectifier are shown on Figure 4. In this diagram it is assumed that anode 1 carries the full direct current, I_c, which is to be commutated and that transfer of this current to anode 2 begins at the instant voltages e_1 and e_2 are equal. Applying Kirchhoff's second law to the commutation circuit formed by phases 1 and 2,

$$e_1-L\frac{di_1}{dt}+L\frac{di_2}{dt}-e_2=0 \quad (23)$$

Since $i_1+i_2=I_c$ by Kirchhoff's first law and $e_2-e_1=e_c$, the commutating voltage of equation 21, this relation reduces to the following forms by substitution:

$$e_c=-2L\frac{di_1}{dt} \quad (24)$$

$$e_c=2L\frac{di_2}{dt} \quad (25)$$

Solving these equations by integration, the anode currents during commutation are

$$i_1=I_c-\frac{\sqrt{2}E_s \sin\pi/p}{X_c}(1-\cos\omega t) \quad (26)$$

and

$$i_2=\frac{\sqrt{2}E_s \sin\pi/p}{X_c}(1-\cos\omega t) \quad (27)$$

where

$X_c=\omega L$

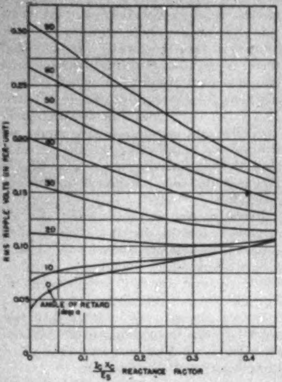


Figure 6. Rms ripple in direct voltage—characteristic curves

$p=3 \quad q=6$
Per unit base is E_{do}

The term commutating current is frequently applied to the circulating current which flows in the commutating circuit during the commutation period and which may be considered to be superposed on the currents existing before commutation. The assumption of such a circulating current is frequently useful in the mathematical analysis of rectifier circuits. The commutating current is equal to current in anode 2 as given by equation 27. As shown on Figure 4 it consists of a d-c component

$$\frac{\sqrt{2}\,E_s \sin \pi/p}{X_c}$$

and a sinusoidal component

$$\frac{-\sqrt{2}\,E_s \sin \pi/p}{X_c} \cos \omega t$$

which lags the commutating voltage e_c by 90 degrees, since the circuit is assumed to be only reactive.

ANGLE OF OVERLAP

The angle of overlap without phase control u may be determined directly from the expression for the anode current and is given by the relation

$$\cos u = 1 - \frac{I_c X_c}{\sqrt{2}E_s \sin \dfrac{\pi}{p}} \tag{28}$$

The foregoing relations for commutating voltage, commutating current, and angle of overlap apply for simple rectifiers where only two phases are commutating at one time. These expressions must be modified in the case of simple rectifiers when the commutating time is so long that more than two phases take part in the commutation. Modification is also required in the case of multiple rectifiers where two or more simple rectifiers operate in parallel with interconnected commutating circuits and commutations involve more than two anodes. In the latter case, if the simple rectifiers are in phase so that commutations take place simultaneously, the foregoing formulas may be applied by considering the total commutating current. If the simple rectifiers are out of phase and the commutations overlap, but are not simultaneous, the calculation becomes more complex.

Considering the double-wye rectifier, the foregoing equations apply for angles of overlap up to 60 degrees. With a greater angle of overlap, commutations on the two wyes overlap. (See references 5 and 6.) The relation between angle of overlap and the rectifier load as calculated from equation 28 is shown graphically on Figure 5. The rectifier load is expressed in terms of the quantity I_cX_c/E_s, which is proportional to the load current.

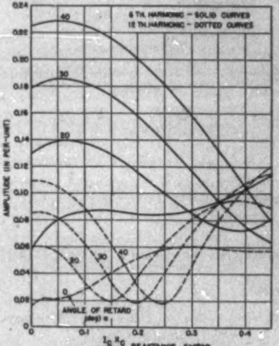


Figure 7. Amplitude of 6th and 12th harmonic voltages in direct-voltage wave—characteristic curves

$p=3 \quad q=6$
Per unit base is E_{do}

REACTANCE FACTOR

The quantity I_cX_c/E_s has been named the "reactance factor."[1] It may be defined as the per unit commutating reactance. Many of the rectifier characteristics, such as regulation, harmonics in a-c input and d-c output wave forms, power factor, and so forth, are functions of the angle of overlap. Inasmuch as the reactance factor I_cX_c/E_s enters directly in the determination of the overlap, and may be calculated readily from the circuit constants, it provides a convenient quantity for indicating the functional relations between the rectifier characteristics and the circuit constants.

The use of per unit quantities greatly facilitates the calculation of rectifier circuits. One set of characteristic curves may be prepared covering each type of rectifier circuit if the circuit constants are represented by the reactance factor I_cX_c/E_s and the rectifier characteristics are expressed in per unit. Then, to obtain information regarding any particular circuit its reactance factor is first calculated from the specific circuit constants. Reference is next made to the characteristic curves and the corresponding per unit value of the desired characteristic determined. Finally, the numerical value may be calculated by multiplying this per unit value by the base value.

COMMUTATING REACTANCE DROP (OVERLAP DROP)

The drop in direct voltage due to overlap arises from the inductive voltage produced by the commutating current. The instantaneous inductive-voltage drop e_L during the commutation period is

$$e_L = L\frac{di_2}{dt} \tag{29}$$

and subtracts from the transformer secondary voltage as shown by the shaded area in Figure 4. The average direct-voltage drop E_x due to the inductive voltage during commutation may be calculated by integration of the inductive voltage drop e_L over the angle u.

$$E_x = \frac{p}{2\pi}\int_0^u e_L d(\omega t) \tag{30}$$

By substitution equation 30 may also be written

$$E_x = \frac{p}{2\pi}L\omega\int_0^{I_c} di_2 \tag{31}$$

Performing the integration and making proper substitutions, the following expression for the voltage drop caused by the commutating reactance is obtained:

$$E_x = \frac{I_cX_cp}{2\pi} \tag{32}$$

where

E_x = the average direct-voltage drop due to overlap
I_c = the direct current commutated
X_c = line-to-neutral commutating reactance in ohms
p = the number of phases in the simple rectifier

The relations between the commutating reactance drop and the other circuit factors may be expressed in various forms by substituting from the different equations. One of the more useful relations is that for the angle of overlap expressed in terms of the overlap drop:

$$\cos u = 1 - \frac{2E_x}{E_{do}} \tag{33}$$

RESISTANCE DROP

The drop in direct voltage due to copper losses may be calculated directly from the losses as follows:

$$E_r = \frac{W_r}{I_d} \qquad (34)$$

where

E_r = average direct-voltage drop due to resistance
W_r = total copper loss in watts
I_d = d-c load current

ARC DROP

The arc drop E_a subtracts directly from the theoretical direct voltage E_{do}.

DIRECT VOLTAGE UNDER LOAD

The rectifier direct voltage under load without phase control E_d may be expressed in terms of the theoretical direct voltage less the voltage drops as follows:

$$E_d = E_{do} - E_x - E_r - E_a \qquad (35)$$

or

$$E_d = E_{do} - \frac{I_c X_c p}{2\pi} - \frac{W_r}{I_d} - E_a \qquad (36)$$

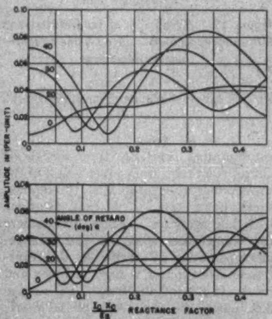


Figure 8. Amplitude of 18th (top) and 24th (bottom) harmonic voltages in direct-voltage wave—characteristic curves

$p = 3 \quad q = 6$

Per unit base is E_{do}

This equation is the one generally used in calculating rectifier regulation. It is frequently convenient to express the voltages in per unit values and the commutating reactance drop as a function of the reactance factor. Equation 36 then becomes

$$\frac{E_d}{E_{do}} = 1 - \frac{1}{2\sqrt{2}\sin\frac{\pi}{p}} \cdot \frac{I_c X_c}{E_s} - \frac{W_r}{I_d E_{do}} - \frac{E_a}{E_{do}} \qquad (37)$$

The general equation for the average direct voltage with overlap may also be derived from the voltage wave of a single rectifying element by integration in the same manner as in the case with no overlap, equation 2. Neglecting resistance drop and arc drop, and assuming no phase control, the resulting equation after performing the integration is

$$E_d = \sqrt{2}E_s\frac{p}{\pi}\sin\frac{\pi}{p}\left(\frac{1}{2}+\frac{1}{2}\cos u\right) \qquad (38)$$

which may also be written

$$E_d = E_{do}\left(\frac{1}{2}+\frac{1}{2}\cos u\right) = E_{do}\cos^2\frac{u}{2} \qquad (39)$$

These relations, while simple in form, require the determination of the angle of overlap and are not as useful as those given by equations 35, 36, and 37.

The inherent direct-voltage regulation curve for a rectifier deviates only slightly from a straight line as the voltage drops E_x and E_r are directly proportional to load current while the arc drop E_a is essentially constant.

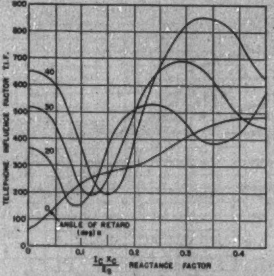


Figure 9. Telephone influence factor for direct voltage—characteristic curves

$p = 3 \quad q = 6$

Harmonics 6, 12, 18, and 24. Weighting factor per reference 5

RIPPLE IN DIRECT VOLTAGE

As in the theoretical case, the magnitude and character of the ripple in the direct voltage of a rectifier operating with overlap may be described in terms of

1. Rms and average values.
2. Harmonic components.
3. Telephone influence factor.

The rms and average values of the ripple voltage with overlap may be calculated in the same way as in the theoretical case. The rms values are plotted as a function of the reactance factor on Figure 6.

If the ripple voltage of the rectifier with overlap is resolved into its harmonic components, the Fourier series will contain both sine and cosine components, as the voltage wave is unsymmetrical when overlapping occurs.[10] The general equations which apply are given in Appendix I. The amplitudes of the first four harmonics are shown on Figures 7 and 8.

The variation in telephone influence factor of the rectifier direct voltage with reactance factor, taking into account only the first four harmonics, is shown on Figure 9.

ALTERNATING CURRENTS

The effect of overlap is to increase the period of conduction without altering the maximum or average value of the anode current. As a result, the rms values of the currents in the transformer and a-c supply circuits are decreased. The rms current with overlapping may be calculated from the values previously calculated for the case with no overlap by multiplying these values by the proper overlap factor.[11]

The overlap factor for correcting the rms value of the a-c wave in a rectifier circuit has the general form

$$\text{Overlap factor} = \sqrt{1 - cf(u)} \qquad (40)$$

where

c = constant whose value depends upon type or part of circuit
(u) = function of angle of overlap

The value of the function $f(u)$ is given by the following expression:

$$f(u) = \frac{1}{2\pi}\left[\frac{\sin u(2+\cos u) - u(1+2\cos u)}{(1-\cos u)^2}\right] \qquad (41)$$

and values are shown on Figure 10.

For the delta–double-wye circuit the constant $c = 3$ and the overlap factor is $\sqrt{1-3f(u)}$ for all parts of the a-c circuit. The variation in overlap factor and reactance factor is shown on Figure 11. Usually no correction is made for overlap in calculating the rms currents in rectifier transformers as the amount of correction is too small to warrant the additional work and if no correction is made for overlap the calculated currents provide a small margin which may be considered to be a safety factor.[11] However, the overlap factor must be taken into account in the calculation of power factor if accuracy is desired.

HARMONICS IN A-C LINE CURRENT

Analytical expressions which include the effect of overlapping may be set up for the a-c waves in the rectifier circuit.[13] From these, the harmonics in the a-c line current may be calculated. The equations which apply are given in Appendix II.

The amplitudes of the first eight harmonics as calculated from the above formulas are shown on Figures 12, 13, 14, and 15. The actual magnitudes of the various harmonics for the delta–double-wye connection with overlap can be calculated by multiplying the value of the fundamental with no overlap given in equation 13 by the per unit values given on the foregoing curves.

The variation in telephone influence factor of the rectifier a-c line current with reactance factor is shown on Figure 16. This curve includes the effect of only the first eight harmonics.

PRIMARY AND SECONDARY VOLT-AMPERES

The primary and secondary volt-amperes are usually calculated only for the case with no overlap. These values are generally used only for making rough comparisons of the different types of circuits and the overlap corrections are not large enough to warrant their application.

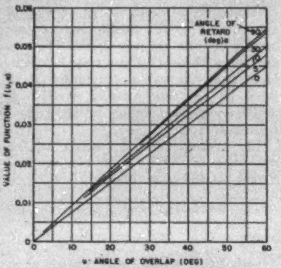


Figure 10. Values of $f(u, \alpha)$

$$f(u,\alpha)=\frac{1}{2\pi}\left[\frac{\sin u[2+\cos(u+2\alpha)]-u[1+2\cos\alpha\cos(u+\alpha)]}{[\cos\alpha-\cos(u+\alpha)]^2}\right]$$

POWER FACTOR

The power factor for the delta-double-wye connection with overlap may be derived by substituting the value for the voltage and current with overlap in the ratio input power to input volt-amperes.

$$\text{Power factor}=\frac{E_d'\,I_d}{\sqrt{3}E_LI_L}=\frac{3}{\pi}\cdot\frac{1}{\sqrt{1-3f(u)}}\cdot\left(1-\frac{E_x}{E_{d0}}\right)\qquad(42)$$

In this equation the effect of resistance drop E_r, arc drop E_a, and transformer magnetizing current I_e is neglected except that the resistance and arc drops may be assumed to be a part of the load so that

$$E_d'=E_d+E_r+E_a$$

If the rms value of the fundamental component of a-c line current is I_1, and the phase angle between the line-to-neutral voltage and I_1 is ϕ, then the power factor is:

$$\text{Power factor}=\frac{\sqrt{3}E_LI_1\cos\phi}{\sqrt{3}E_LI_L}\qquad(43)$$

where

E_L = rms value of line-to-line voltage
I_L = rms value of a-c line current

This equation shows that the power factor is the product of two factors; namely, the displacement factor, $\cos\phi$, and the distortion factor, I_1/I_L. The displacement factor may be calculated from the expressions for the fundamental components of the a-c line current (see Appendix II, equations 73 and 77).

$$\cos\phi=\frac{a_1}{c_1}=\frac{\sin^2 u}{\sqrt{u^2+\sin^2u-2u\sin u\cos u}}\qquad(44)$$

The distortion factor is most readily calculated from equations 42 and 44. The power factor, displacement, and distortion factors for the delta-double-wye rectifier are shown on Figure 17.

INTERPHASE-TRANSFORMER VOLTAGE

The interphase-transformer voltage wave form is changed and the rms and average values increased by the overlap. The formulas for calculating the voltages with overlap are quite involved and will not be given here. The per unit values of rms and average voltage across the interphase as a function of the reactance factor are shown on Figure 18.

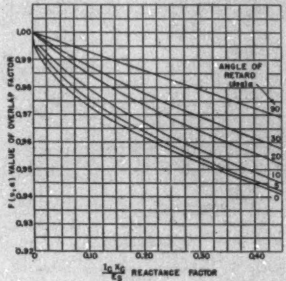


Figure 11. Overlap factor—characteristic curve

$p=3$

$$\text{Overlap factor}=\sqrt{1-3f(u,\alpha)}$$

VOLTAGE ACROSS RECTIFYING ELEMENT

The peak inverse voltage remains the same with overlap as in the case with no overlap.

DETERMINATION OF COMMUTATING REACTANCE DUE TO MAIN TRANSFORMER

The commutating reactance due to the main transformer may be determined directly by test. The usual method employed in making this test is to short-circuit the primary windings and apply a single-phase sinusoidal voltage of normal frequency between two secondary anode leads between which a transfer of current occurs during commutation. For the delta-double-wye circuit, the current in the secondary is held at a value obtained by dividing the primary kilovolt-amperes per phase by two times the secondary line-to-neutral voltage,* and the impedance voltage drop between the two secondary terminals is measured. The anode-to-anode commutating reactance is obtained by dividing the measured impedance voltage by the applied current. The commutating reactance anode-to-neutral is half of this value.

It is frequently desired to calculate the commutating reactance from the short-circuit reactance (impedance volts) of the transformer as measured by three-phase short-circuit test. This may be done with fair accuracy in some cases where the transformer core and coil arrangement permits. In other cases the transformer construction is more complex and results in a configuration of the leakage flux paths for the single-phase short-circuit test (commutating reactance test) different from that for the standard three-phase short-circuit test and so introduces other factors into the relations between the two reactances.

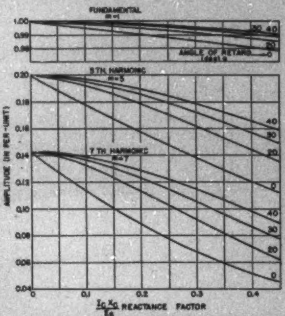


Figure 12. Amplitude of fundamental and fifth and seventh harmonics in a-c line current—characteristic curves

$p=3 \qquad q=6$

Per unit base is amplitude of fundamental with no overlap

In the case of the delta-double-wye circuit the commutating reactance X_c in ohms (anode-to-neutral) is

$$X_c=\frac{3E_s^2}{\sqrt{3}E_LI_L}\cdot\underline{X}_T\qquad(45)$$

where

E_s = rms value of transformer secondary to neutral voltage
$\sqrt{3}E_LI_L$ = transformer volt-amperes
$\underline{X}_T$ = per unit reactance of transformer expressed on base of transformer volt-amperes at rated load (base values should be used for other terms)

---

* Conventional value. If this value is held, primary current will not exceed rating.

This equation is based on the assumption that the two secondary windings on each leg are perfectly interlaced.

It is frequently convenient to use equation 45 in a different form, expressing the reactance in terms of the base value of direct current I_d instead of the transformer volt-amperes. By substitution of the value of transformer volt-amperes given by equation 14, the commutating reactance due to the transformer becomes

$$X_c=\frac{\sqrt{6}E_s}{I_d}\cdot\underline{X}_T \qquad (46)$$

Transformation of Line Reactance to Commutating Reactance

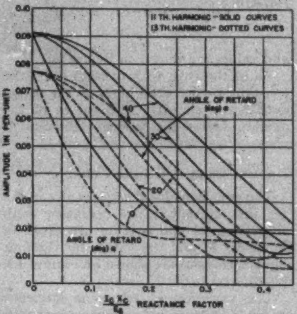


Figure 13. Amplitude of 11th and 13th harmonics in a-c line current—characteristic curves

$p=3 \quad q=6$

Per unit base is amplitude of fundamental with no overlap

The equivalent commutating reactance X_c, in ohms anode to neutral, due to reactance of the a-c line X_L, in ohms per line, may be calculated by means of the transformation,

$$X_c=3\left(\frac{E_s}{E_L}\right)^2 X_L \qquad (47)$$

where

E_s/E_L = turn ratio

Reactance Factor in Terms of Transformer and Line Reactance

The reactance factor for the delta-double-wye circuit may be determined directly from the per unit transformer and line reactances, $\underline{X}_T$ and $\underline{X}_L$, respectively, by the transformation.

$$\frac{I_dX_c}{E_s}=\frac{\sqrt{3}}{\sqrt{2}}(\underline{X}_T+\underline{X}_L)=1.225(\underline{X}_T+\underline{X}_L) \qquad (48)$$

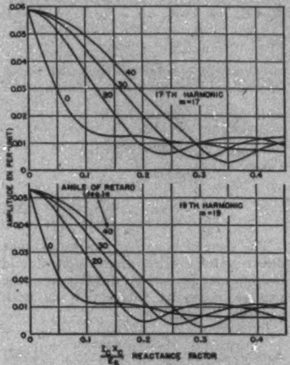


Figure 14. Amplitude of 17th and 19th harmonics in a-c line current—characteristic curves

$p=3 \quad q=6$

Per unit base is amplitude of fundamental with no overlap

D-C Short-Circuit Current

The short-circuit current which flows when the d-c terminals of the rectifier are short-circuited depends upon the value of the a-c circuit reactances and their distribution. In most installations with a delta-double-wye transformer the reactance may be considered to be located in the primary, as the secondary windings are usually interlaced and the reactance in the anode leads is inappreciable. In this case each anode may be assumed to conduct current for only 180 degrees and of sinusoidal form so that the transformer primary current is the same as would be obtained if a three-phase short circuit were applied to one secondary wye. The sustained d-c short-circuit current in average amperes is then

$$I_{sc}=\frac{2\sqrt{2}}{\pi}\cdot\frac{\sqrt{3}E_LI_L}{E_s(\underline{X}_T+\underline{X}_L)} \qquad (49)$$

or

$$I_{sc}=\frac{2\sqrt{3}}{\pi}\cdot\frac{I_d}{(\underline{X}_T+\underline{X}_L)}=1.103\frac{I_d}{(\underline{X}_T+\underline{X}_L)} \qquad (50)$$

where the various quantities have the same values as in equations 45 and 46.

Analysis With Phase Control and Overlap

When the rectifier is operated with the firing retarded by phase control (obtained by means of either grids or ignitors), the voltage and current wave form and circuit characteristics, derived for the case with overlap, undergo further modifications. The wave forms with both phase control and overlap are shown on Figure 1 under case III. The equations giving the rectifier characteristics under these conditions will be derived by introducing the additional factor of phase angle α in the expressions already derived for the characteristics with the angle of overlap.

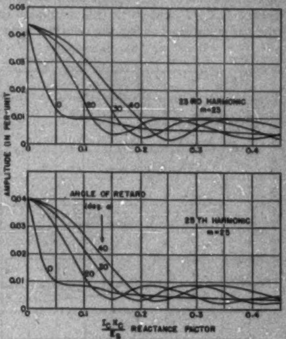


Figure 15. Amplitude of 23d and 25th harmonics in a-c line current—characteristic curves

$p=3 \quad q=6$

Per unit base is amplitude of fundamental with no overlap

Commutating Voltage

The commutating voltage in the case of a simple rectifier operating with phase control is the same as in the case without phase control except that commutation is not permitted to start at the instant that the voltages (on the anode which is firing, and on the anode which fires next in order) become equal. By the action of the phase control, the firing of the main anode is delayed by a time interval equal to the angle of phase retard and commutation does not start until α degrees after the point at which the anode voltages are equal. The commutating voltage, measuring time from the instant that commutation begins, is given by the equation:

$$e_c=2\sqrt{2}E_s\sin\frac{\pi}{p}\sin(\omega t+\alpha) \qquad (51)$$

Commutating Reactance

The reactance which is effective in the commutating circuit when operating with phase control is the same as for the case with a simple rectifier operating with overlap.

Commutating Current

The expression for the anode current during the commutation period, when operating with phase control, may be derived in the same manner as for the simple case with overlap. The commutating current is the current which flows in anode 2 during the commutation period at the beginning of conduction, and is given by the following equation:

$$i_2 = \frac{\sqrt{2}E_s \sin \pi/p}{X_c}[\cos\alpha - \cos(\omega t+\alpha)] \quad (52)$$

Angle of Overlap

The angle of overlap with phase control follows directly from the equation for the commutating current and is

$$\cos(u+\alpha) = \cos\alpha - \frac{I_cX_c}{\sqrt{2}E_s \sin\frac{\pi}{p}} \quad (53)$$

It will be noted that this expression for the angle of overlap with phase control reduces to that for the simple rectifier with overlap when the angle of retard α is equal to zero. Also, that the reactance factor I_CX_C/E_s enters in this equation in the same manner as for the overlap equation without phase control. The relation between the angle of overlap and the reactance factor for various angles of retard as calculated from equation 53 is shown graphically on Figure 5.

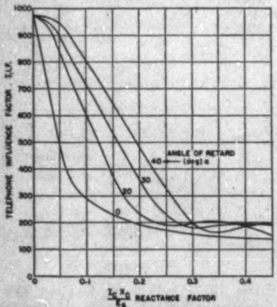


Figure 16. Telephone influence factor for a-c line current—characteristic curves

$p=3 \quad q=6$

Harmonics 1, 5, 7, 11, 13, 17, 19, 23, and 25; weighting factor per reference 5

Commutating Reactance Drop (Overlap Drop)

The drop in direct voltage due to overlap with phase control may be calculated in the same manner as for the case of the simple rectifier without phase control, and is found to be the same as is given in equation 32, namely

$$E_x = \frac{I_cX_cp}{2\pi}$$

This fact shows that the voltage drop due to the transformer reactance (overlapping) is independent of the use of phase control.

Resistance and Arc Drops

The resistance and arc drops, when operating with phase control, are substantially the same as for the case of the simple rectifier with overlap. The only appreciable change in the resistance drop is that due to the increased losses in the interphase transformer, resulting from the higher voltage impressed across it when operating with phase control.

Direct Voltage Under Load

The direct voltage of the rectifier when operating with phase control and overlap is

$$E_d = E_{do}\cos\alpha - E_x - E_r - E_a \quad (54)$$

or

$$E_d = E_{do}\cos\alpha - \frac{I_cX_cp}{2\pi} - \frac{W_r}{I_d} - E_a \quad (55)$$

or in per unit values

$$\frac{E_d}{E_{do}} = \cos\alpha - \frac{1}{2\sqrt{2}\sin\frac{\pi}{p}} \cdot \frac{I_cX_c}{E_s} - \frac{W_r}{E_{do}I_d} - \frac{E_a}{E_{do}} \quad (56)$$

The general equation for the average direct voltage with phase control and overlap may be derived directly from the voltage wave by integration as with equation 2, and is

$$E_d = \sqrt{2}E_s\frac{p}{\pi}\sin\frac{\pi}{p}\left[\frac{\cos\alpha}{2} + \frac{\cos(u+\alpha)}{2}\right] \quad (57)$$

This equation is equal to equation 54 or 55 if resistance drop and arc drop are neglected.

Ripple in Direct Voltage

As in the two previous cases, the magnitude and character of the ripple in the direct voltage may be described in terms of:

1. Rms and average values.
2. Harmonic components.
3. Telephone influence factor.

The rms values of the ripple voltage with both phase control and overlap are shown on Figure 6.

The equations for the amplitude of the harmonic components of the ripple voltage with phase control are given in Appendix I, and the values for the first four harmonics are given on Figures 7 and 8.

The telephone influence factor of the rectifier direct voltage with various amounts of phase control is shown on Figure 9.

Alternating Currents

When the rectifier is operated with phase control the angle of overlap is reduced because a higher voltage is available for forcing commutation. As a result the rms values of the currents in the transformer and a-c supply circuits will lie between those obtained in the theoretical case (no overlap) and those for the case with overlap but no phase control. The overlap factor for correcting the rms value of the a-c wave with both phase control and overlap has the general form

$$\text{Overlap factor} = \sqrt{1-cf(u,\alpha)} \quad (58)$$

where

c = constant whose value depends upon type or part of circuit (see equation 40)
$f(u,\alpha)$ = function of angles of overlap and phase retard

The value of the function $f(u,\alpha)$ is given by the following expression

$$f(u,\alpha) = \frac{1}{2\pi}\left[\frac{\sin u(2+\cos(u+2\alpha)) - u(1+2\cos\alpha\cos(u+\alpha))}{(\cos\alpha - \cos(u+\alpha))^2}\right] \quad (59)$$

and values are shown on Figure 10.

As in the case with overlap only, the constant $c=3$ for all parts of the delta-double-wye circuit. The values of the overlap factor for various amounts of phase retard are shown on Figure 11.

Harmonics in A-C Line Current

The equations for the harmonics in the a-c line current when operating with phase control and overlap are given in Appendix II and the values for the first eight harmonics are shown on Figures 12, 13, 14, and 15.

The variation in telephone influence factor of the rectifier a-c line current with phase control is shown on Figure 16.

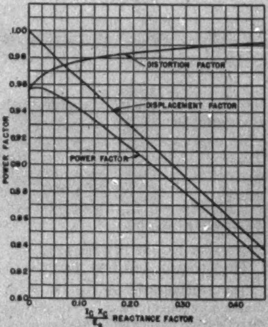


Figure 17. Power factor without phase control—characteristic curves

$p=3 \quad q=6$

$$Pf = \frac{3}{\pi} \cdot \frac{1}{\sqrt{1-3f(u,\alpha)}} \cdot \left(1 - \frac{E_x}{E_{do}}\right)$$

$$\cos\phi = \frac{a_1}{c_1} = \frac{\sin^2 u}{\sqrt{u^2+\sin^2 u - 2u\sin u\cos u}}$$

POWER FACTOR

The expression for the power factor with both phase control and overlap is similar to that when operating with overlap only, and is as follows:

$$Pf=\frac{3}{\pi}\cdot\frac{1}{\sqrt{1-3f(u,\alpha)}}\cdot\left(\cos\alpha-\frac{E_x}{E_{do}}\right) \quad (60)$$

INTERPHASE-TRANSFORMER VOLTAGE

The voltage across the interphase transformer is increased with the application of phase control. The rms and average voltages across the interphase transformer for various values of phase control are shown on Figure 18.

VOLTAGE ACROSS RECTIFYING ELEMENT

The peak inverse voltage remains the same when operating with both phase control and overlap as in the theoretical case as long as $(u+\alpha)$ is less than 90 degrees.

Conclusions

The foregoing analysis was originally undertaken because of the need for a reference text giving the complete analysis of a typical rectifier circuit. The analytic procedure which has been outlined was developed in the course of its preparation.

The purposes of this paper have been to:

1. Describe in concise form the methods used in analyzing rectifier circuits.
2. Illustrate the application of new circuit concepts.
3. Provide a standard procedure for the analysis of all types of rectifier circuits.
4. Provide a standard form for the presentation of rectifier characteristics.
5. Present a comprehensive set of characteristic curves for the delta-double-wye rectifier.

It is hoped that the material presented in the paper will be found useful by design, application, and operating engineers and that the methods may be extended to cover the other rectifier circuits in common use.

Appendix I. Harmonics in Direct Voltage

The general equation for the direct-voltage wave with phase control and overlap is

$$e=E_d+a_1\sin q\theta+a_2\sin 2q\theta+a_3\sin 3q\theta+\ldots+a_m\sin mq\theta+\ldots+b_1\cos q\theta+b_2\cos 2q\theta+b_3\cos 3q\theta+\ldots+b_m\cos mq\theta+\ldots \quad (61)$$

where

e = instantaneous direct voltage
E_d = average direct voltage
a_1, a_2, etc. = amplitude of sine components
b_1, b_2, etc. = amplitude of cosine components
q = number of phases of equivalent simple rectifier having same wave shape

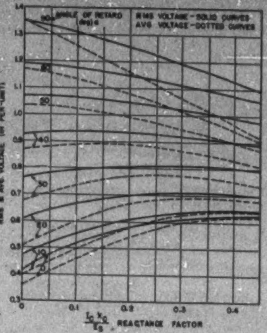


Figure 18. Rms and average voltage across interphase transformer—third harmonic

$p=3 \quad q=6$

Per unit base is transformer secondary-to-neutral voltage E_s

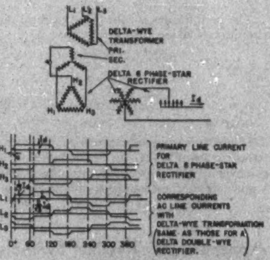

L1
L2
L3
DELTA-WYE TRANSFORMER
PRI.
SEC.
DELTA 6 PHASE-STAR RECTIFIER
H1
H2
H3
I_d
PRIMARY LINE CURRENT FOR DELTA 6 PHASE-STAR RECTIFIER
L1
L2
L3
CORRESPONDING AC LINE CURRENTS WITH DELTA-WYE TRANSFORMATION (SAME AS THOSE FOR A DELTA DOUBLE-WYE RECTIFIER.)
0°
60
120
180
240
300
360

Figure 19. Transformation of wave shape of rectifier a-c line current

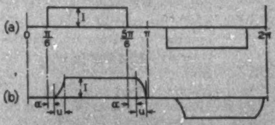


Figure 20. Wave form of a-c line current on delta–six-phase-star circuit

(a) Theoretical
(b) With phase control and overlap

m = 1, 2, 3, etc., order of harmonic
θ = angle measured from instant transfer begins

The amplitude of the sine components is

$$a_m=\frac{E_{do}}{2}\cos m\pi\times\left[\frac{\sin(mq+1)(u+\alpha)+\sin(mq+1)\alpha}{mq+1}-\frac{\sin(mq-1)(u+\alpha)+\sin(mq-1)\alpha}{mq-1}\right] \quad (62)$$

and of the cosine components

$$b_m=\frac{E_{do}}{2}\cos m\pi\times\left[\frac{\cos(mq+1)(u+\alpha)+\cos(mq+1)\alpha}{mq+1}-\frac{\cos(mq-1)(u+\alpha)+\cos(mq-1)\alpha}{mq-1}\right] \quad (63)$$

In the case with overlap only (no phase control) these expressions reduce to

$$a_m=\frac{E_{do}}{2}\cos m\pi\times\left[\frac{\sin(mq+1)u}{mq+1}-\frac{\sin(mq-1)u}{mq-1}\right] \quad (64)$$

and

$$b_m=\frac{E_{do}}{2}\cos m\pi\times\left[\frac{\cos(mq+1)u}{mq+1}-\frac{\cos(mq-1)u}{mq-1}-\frac{2}{m^2q^2-1}\right] \quad (65)$$

The amplitude of the resultant of the various harmonics is

$$c_m=\sqrt{a_m^2+b_m^2}$$

Appendix II. Harmonics in A-C Line Current

The a-c line current of a rectifier having six phases may be represented by the following Fourier series:

$$i=a_1\sin\theta+a_5\sin 5\theta+a_7\sin 7\theta+a_{11}\sin 11\theta+a_{13}\sin 13\theta\ldots+a_m\sin m\theta\ldots+b_1\cos\theta+b_5\cos 5\theta+b_7\cos 7\theta+b_{11}\cos 11\theta+b_{13}\times\cos 13\theta\ldots+b_m\cos m\theta\ldots \quad (66)$$

where

i = instantaneous primary line current
a_1, a_5, a_7, etc. = amplitude of sine components
b_1, b_5, b_7, etc. = amplitude of cosine components
$m=nq\pm 1$ order of harmonic where n = integer 1, 2, 3, etc.
q = number of phases of equivalent simple rectifier having wave shape with same harmonic composition
θ = angle measured from reference point coinciding with beginning of line-to-neutral voltage wave

It can be shown by analysis that the harmonic composition of the a-c line currents in the various rectifier circuits having an equal number of secondary phases is the same if certain assumptions as to the shape of the current in each secondary phase are made.[14] The necessary assumptions in order that the per unit values of the harmonics for different connections be the same are that

1. The anode current waves are flat-topped.
2. The angles of overlap are equal.
3. The commutations do not overlap.

While the shape of the a-c line current wave may be different for the various rectifier circuits having the same number of secondary phases, the magnitudes of the harmonics will be equal if the foregoing conditions are fulfilled. The difference in wave shape arises from differences in phase relations between harmonics. In general, the wave obtained when using one circuit may be changed to that obtained with a different circuit by means of a suitable transformation. An example is the delta–six-phase-star circuit shown on Figure 19 and the delta–wye transformation from the delta–six-phase-star wave shape to the delta–double-wye wave shape. Taking advantage of these relationships to simplify the analysis of rectifier wave shapes, calculations of the magnitude of the harmonics in the a-c line current of all six-phase rectifier circuits are based on analyses made on the delta–six-phase-star connection.

The coefficients of the Fourier series, equation 66, for the delta–six-phase-star connection operating with both phase control and overlap, and with the angle θ measured from a reference point coinciding with the beginning of the line-to-neutral voltage wave (see Figure 20), are given by the following formulas. The coefficients of the fundamental components are

$$a_1=\frac{\sqrt{3}I}{\pi}[\cos\alpha+\cos(u+\alpha)] \tag{67}$$

$$b_1=\frac{\sqrt{3}I}{\pi}\left[\frac{\sin(2u+2\alpha)-\sin 2\alpha-2u}{2[\cos\alpha-\cos(u+\alpha)]}\right] \tag{68}$$

and the coefficients of the higher harmonics are

$$a_m=\frac{4I}{\pi}\cdot\frac{\sin(m\pi/3)}{m(m^2-1)[\cos\alpha-\cos(u+\alpha)]}\times [\cos\alpha\cos m\alpha-\cos(u+\alpha)\times \cos m(u+\alpha)+m\sin\alpha\sin m\alpha- m\sin(u+\alpha)\sin m(u+\alpha)] \tag{69}$$

$$b_m=\frac{4I}{\pi}\cdot\frac{\sin(m\pi/3)}{m(m^2-1)[\cos\alpha-\cos(u+\alpha)]}\times [\cos\alpha\sin m\alpha-\cos(u+\alpha)\times \sin m(u+\alpha)-m\sin\alpha\cos m\alpha+ m\sin(u+\alpha)\cos m(u+\alpha)] \tag{70}$$

In these equations, I = peak value of a-c line current wave in delta–six-phase-star connection.

The amplitude of the resultant of the fundamental is

$$c_1=\sqrt{a_1^2+b_1^2} =\frac{\sqrt{3}I}{\pi}\left[\frac{1}{\cos\alpha-\cos(u+\alpha)}\right]\times \sqrt{u^2-u\sin(2u+2\alpha)+u\sin 2\alpha+\sin^2 u} \tag{71}$$

and the amplitude of the resultant of each of the higher harmonics is

$$c_m=\sqrt{a_m^2+b_m^2} =\frac{4I}{\pi}\cdot\frac{\sin(m\pi/3)}{m(m^2-1)[\cos\alpha-\cos(u+\alpha)]}\cdot\times \sqrt{[\cos^2\alpha+\cos^2(u+\alpha)+m^2\sin^2\alpha+ m^2\sin^2(u+\alpha)-2\cos\alpha\times \cos(u+\alpha)\cos mu-2m\sin u\times \sin mu-2m^2\sin\alpha\sin(u+\alpha)\times \cos mu]} \tag{72}$$

When operating with overlap only (no phase control) these expressions reduce to

$$a_1=\frac{\sqrt{3}I}{\pi}[1+\cos u] \tag{73}$$

$$b_1=\frac{\sqrt{3}I}{\pi}\left[\frac{\sin u\cos u-u}{1-\cos u}\right] \tag{74}$$

$$a_m=\frac{4I}{\pi}\left[\frac{1-\cos u\cos mu-m\sin u\sin mu}{m(m^2-1)(1-\cos u)}\right]\times \sin(m\pi/3) \tag{75}$$

$$b_m=\frac{4I}{\pi}\left[\frac{-\cos u\sin mu+m\sin u\cos mu}{m(m^2-1)(1-\cos u)}\right]\times \sin(m\pi/3) \tag{76}$$

$$c_1=\frac{\sqrt{3}I}{\pi}\cdot\frac{1}{1-\cos u}\sqrt{u^2-u\sin 2u+\sin^2 u} \tag{77}$$

$$c_m=\frac{4I}{\pi}\cdot\frac{\sin(m\pi/3)}{m(m^2-1)(1-\cos u)}\times \sqrt{[1+\cos^2 u+m^2\sin^2 u- 2\cos u\cos mu-2m\sin u\sin mu]} \tag{78}$$

The amplitude of the fundamental of the theoretical a-c line current wave is

$$a_1=\frac{2\sqrt{3}I}{\pi}$$

$$b_1=0$$

All amplitudes may be expressed in per unit, using the amplitude of the fundamental with no overlap as a base. To obtain per unit values, divide these equations by $(2\sqrt{3}I)/\pi$.

Appendix III. Rectifier Symbols

u = angle of overlap (commutating angle)
α = phase-control angle (the angle by which the start of conduction is retarded or advanced from the normal starting time by phase control)
f = frequency of a-c power supply
p = number of phases in a simple rectifier
q = number of phases in an equivalent simple rectifier having the same primary wave shape as the multiple rectifier
E_s = transformer secondary line-to-neutral voltage in rms volts
E_L = primary line-to-line voltage in rms volts
E_{do} = theoretical direct voltage (average direct voltage assuming no overlap, no phase control, and zero arc drop)
E_d = average direct voltage under load
E_a = direct-voltage drop due to loss in the rectifying elements (arc drop in volts)
E_r = direct-voltage drop due to copper losses
E_x = direct-voltage drop caused by commutating reactance
e_{pi} = peak inverse voltage
I_d = d-c load current supplied by the rectifier in average amperes
I_c = direct current commutated between two rectifying elements which conduct consecutively
I = a-c line current (crest value)
I_s = coil current of transformer d-c winding in rms amperes
I_p = coil current of transformer a-c winding in rms amperes
I_L = a-c line current in rms amperes
I_e = transformer exciting current in rms amperes
a_m = amplitude of sine term of Fourier expansion for mth harmonic (crest value)
b_m = amplitude of cosine term of Fourier expansion for mth harmonic (crest value)
c_m = amplitude of resultant of Fourier expansion for mth harmonic (crest value)
m = order of harmonic $= nq\pm 1$ where n = integer 1, 2, 3, etc.
$\underline{X}_T$ = per unit reactance of transformer, expressed on base of transformer volt-amperes at rated load
$\underline{X}_L$ = per unit reactance of supply line, expressed on base of transformer volt-amperes at rated load
X_L = ohms reactance of supply line (per line)
X_c = commutating reactance line-to-neutral in ohms
L_d = inductance of d-c reactor in henrys
I_cX_c/E_s = reactance factor

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The Electronic Converter for Exchange of Power

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Synopsis: The installation of the first 20,000-kw electronic power converter was made in December 1943 at the Edgar Thompson plant of the Carnegie-Illinois Steel Corporation to exchange power between their 25-cycle 44-kv and 60-cycle 69-kv systems, the latter being connected to a large public utility. The converter operates in parallel on both systems with normal generators. This type of converter was selected to obtain its ease and simplicity of control, low contributions to faults, light weight of indoor equipment, high efficiency, and ability to transfer a fixed value of power regardless of departure from normal frequency ratio between the two systems. The converter may have its power output controlled manually or by a power regulator or may reduce the demands for power taken from the 60-cycle utility system. Controls also may be supplied to a typical electronic converter to cause it to share the total load of the receiving system in a predetermined proportion with prime movers. Inherently transferring power in a preselected direction in relation to the departure from normal voltage ratios of the two systems, automatic phase control of grid excitation rapidly compensates for changes in this ratio. The reactive kilovolt-amperes needed for commutation, harmonics, and transformer magnetizing current of both rectifier and inverter is supplied by the synchronous machinery in the steel company's systems.

Faults such as arc-backs and "shoot-throughs" in the electronic equipment are suppressed in a very short time period. The rectifier cannot furnish current to a fault in its supply system. The inverter can supply only a small increase over load current to a fault in the receiving system. When the fault in the receiving system depresses its voltage below commutating limits, the inverter instantly ceases to commutate and consequently furnishes no current to the fault. After release of the fault from the receiving system, the converter will pick up load in a maximum of four-tenths second.

Distinguishing characteristics of conventional rotating converters are compared with those of the electronic converter. The operating characteristics of the rectifier and the inverter are explained, and expressions are given for their power factors and arc drop. Characteristic power factors, reactive kilovolt-amperes, and efficiency are shown in charts, and a description of operation under fault conditions is given. Details of the installation, including circuit diagram, specification of major apparatus, and floor space, are included.

The authors conclude that the electronic converter is suitable for controlling power flow between systems of like or dissimilar frequency and voltage or for furnishing power to isolated loads which have sufficient synchronous capacity for commutation at fixed or variable frequencies. When required, the d-c link between rectifier and inverter may be lengthened to the classification of a transmission system. Interconnection of systems may be accomplished in a number of different ways, each having certain characteristics. The electronic converter may prove advantageous in some instances; rotating converters will be more advantageous in other instances.

THE interconnection of electric power systems involves many considerations—one major operating consideration being the control of power flow. This and associated papers[8–11] discuss a controlled electronic means of power interchange, known in principle for some time.

Since August 1936 the principles of electronic rectification of alternating to direct current, transmission of direct current, and electronic inversion from direct to alternating current have been put into commercial use in amounts up to 5,250 kw in a pilot plant,[1,2] jointly owned by the New York Power and Light Corporation and the General Electric Company.

A group of Carnegie-Illinois Steel Corporation engineers, led by one of the coauthors, made a thorough review of electronic-converter developments in 1940–41, as a result of which the General Electric Company was asked to undertake the manufacture and installation of two 10,000-kw electronic converters to transfer power, reversibly, between Carnegie-Illinois Steel Corporation's 25-cycle 44-kv and 60-cycle 69-kv systems, each having its own generators. Their 60-cycle plants are connected to the system of a public utility having over 60,000 kw in generators. The steel company's 25-cycle system has over 125,000 kw in generators. (See Figure 1.) During December 1943 the first 10,000-kw converter was put into operation; after a few weeks' interval, the second unit was put into operation.

The preference given to an electronic converter for the Carnegie plant depended not only on the benefits of less material and shorter production time, but also on its particular electrical and physical characteristics.

Power-System Interconnection

The Carnegie electronic converter described herein becomes better understood if the general problem of system interconnection and the technical features of various types of interconnection are briefly reviewed. In giving the characteristics of various power converters, no attempt is made to draw conclusions as to the generally preferred type or as to the relative economics of various types. To draw such conclusions would require each case to have its engineering specifications clearly defined, after which an economic comparison of suitable types could be made. The results of such a comparison would show which type is best suited to the case.

The reader's attention is called to the characteristics of various types of converters, given herein, to explain more clearly the features of the electronic converter. In the opinion of the authors the electronic converter will not take the place of rotating converters in all cases.

OBJECTIVES OF POWER-SYSTEM INTERCONNECTION

Numerous electric power systems now extend widely from energy sources until their lines have become geographically continuous or even overlapping. Some depend on source energy which varies in availability seasonally and from day to day. Others depend on transported fuel, the reliability of which is not always within the control of the user. Inherent economies of the supply system are often in favor of one system over another. In a given time period one system may have a power surplus without market and during another period may have to resort to higher-cost sources to meet requirements. During these periods a neighboring system may assist to the mutual benefit of both.

CONVENTIONAL METHODS USED TO INTERCONNECT SYSTEMS AND MEANS ADOPTED TO CONTROL TRUE POWER AND REACTIVE FLOW

Systems of like frequency use direct conductors or constant-potential transformers to effect interconnection. Reactive current through the interconnection may be controlled by variable-ratio transformers and by the synchronous machines of the systems. Power flow is controlled by relative prime-mover-governor adjustment. Power may flow in either direction.

Systems of dissimilar frequency generally have used rotating frequency converters of various types. The synchronous–synchronous type requires the frequency ratio of the two systems to remain

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fixed and the converter to have sufficient torque to maintain this ratio.

Converters which utilize the inherent slip of machines designed on the induction principle permit the frequency ratio of the two systems to depart from normal by the amount of the slip incorporated in the machine design with flow of power a consequent result. Variable-ratio frequency converters diminish the need for modifying the relative prime-mover-governor adjustment of the two systems to maintain a constant power flow. The wider the designed frequency ratio of this converter, the more material must be used.

Both the synchronous–synchronous and Scherbius induction–synchronous converters have the desirable characteristics of controlling reactive current to both systems and of furnishing power to independent inductive loads from machines of both frequencies. The simple but little used induction–induction converter must get its a-c excitation from the two systems.

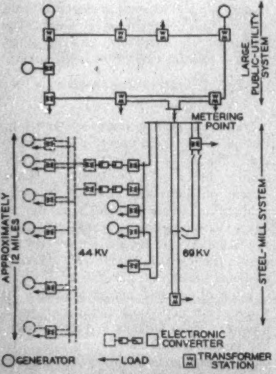


Figure 1. Electronic-converter interconnection of 60-cycle and 25-cycle systems

----25-cycle system ——60-cycle system

Power flow may be in either direction through all of the rotating types of converters mentioned.

In summary, then, it may be said that the distinguishing characteristics of rotating converters are:

1. Power is transmitted through the converter as a function of frequency and may be controlled by the use of certain variable-frequency-ratio converters.

2. Synchronous–synchronous and Scherbius types of rotating converters have the ability to supply and control overexcited reactive current to the systems to which they are connected.

3. The same types can furnish power to independent inductive or capacitive loads.

4. All rotating types are sources of short circuit currents, the system being relieved of the fault in the opening time of conventional circuit breakers.

5. These types of rotating converters resist a change in voltage due to disturbances and are capable of carrying a temporary overload, and therefore are a factor in improving the voltage and the stability of a system.

THE ELECTRONIC POWER CONVERTER AS A MEANS OF INTERCONNECTING SYSTEMS AND CONTROLLING POWER FLOW

The electronic power converter is also a means of interconnecting two electric systems. In its form, as discussed in this paper, it rectifies alternating to direct current at the supply end and then inverts direct to alternating current at the receiving end. Except for minor auxiliaries, such as water-circulating pumps, small Amplidynes, and servomotor controls, it is a completely static equipment. Excitation for commutation, and so forth, is supplied by synchronous apparatus.

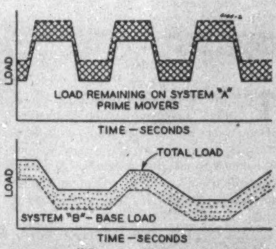


Figure 2. When the direction of power flow is from system *B* to system *A*, a uniform amount of power (crosshatched) may be transferred from system *A* by the electronic converter and added (dotted) to the load of system *B*

When electronic apparatus is used to convert a-c power to d-c power it is called a *rectifier*. The same apparatus can be used to reconvert d-c power to a-c power by means of simple adjustments of its control; when so operated it is called an *inverter*. That portion of the system in which d-c power flows is called the d-c link. The combination has been termed a converter. The d-c link may be lengthened, until ultimately it might be termed a d-c power transmission system.

The electronic converter affords a means of controlling power flow between the two otherwise independent systems which it connects. The direction of power flow is determined by the phase angle of the grid excitation. When no intentional internal electronic controls intervene, the direct voltages of rectifier and inverter bear direct relations to the alternating voltages of the supply and receiving systems. Insofar as this relationship is concerned, a similar characteristic occurs in a synchronous rotating converter. Power flow in an electronic converter depends on the relative magnitude of the direct voltages of the supply or rectifier end and inverter or receiving end. Current, and therefore power, flows through the d-c link when anything tends to raise the rectifier voltage above the inverter countervoltage. Why this is true is discussed later under "Operation" and "Characteristics."

The relative levels of direct voltage may be varied by "phase control." The direction of power flow may be reversed by a simple operation in the control circuits. The conclusion to be reached from these statements is that, with phase-angle controls set for power flow in one direction and with the phase angle of grid excitation fixed, power will flow through the converter when the voltage of the system supplying the rectifier is relatively higher than the voltage of the system supplied by the inverter.

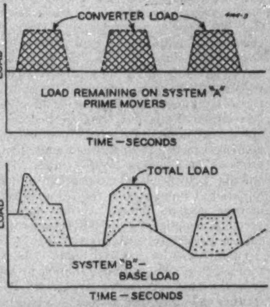


Figure 3. Demand control applied to the electronic converter removes peaks (crosshatched) from system *A* and places them (dotted) on system *B*

If a-c system loads and transmission losses were constituted of power-current components only, the power output of the converter would be approximately proportional to current. Current control of the converter then would constitute control of power output. However, reactive and capacitive system currents affect system voltages and, therefore, affect input and output current of the converter. Consequently, to control the output of an electronic converter in terms of power, a watt-sensitive regulator may be, and in this installation is, used to hold the power output at a constant level. In summary then, the phase-control setting determines ampere output of the converter regardless of frequency variations, and a power regulator determines watt output regardless of both frequency variation and voltage-ratio variation of the two connected systems.

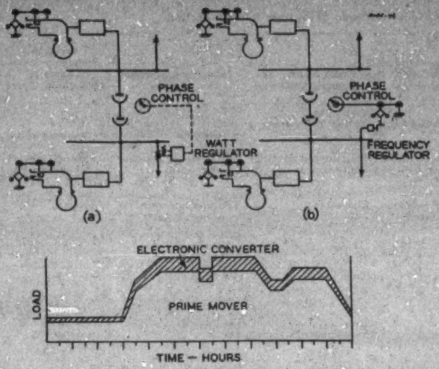


Figure 4. Electronic converter controlled to share loads with prime movers

(*a*)—Converter output in proportion to total load of one system

(*b*)—Converter output in response to frequency of one system

Since an electronic converter characteristically forms a controlled power interconnection, it is possible to provide controls responsive to other signals. Summarizing those that appear useful, controls are available for:

1. Constant power output to one system (see Figure 2).

2. Convenient reversibility of power output in response to a predetermined signal.

3. Power output in response to the requirements of a specified portion of a general load (see Figure 3).

4. Power output in response to the variations in total load of one system and in a predetermined ratio to the power supplied by other prime sources of that system (see Figure 4).

5. Power output required to maintain the desired frequency of one system or with frequency-regulation characteristics suitable for parallel operation with prime movers (see Figure 4).

The function of rectifier and inverter commutation, as explained later, requires overexcited reactive current. Such current must be supplied by the overexcited rotating machines of each system and in amounts proportional to the power transfer.

Distinguishing Characteristics

The characteristics of an electronic converter which distinguish it from other types of converters are given in outline here and are discussed in further detail in companion papers:

1. Current, without respect to frequency, is transmitted through the converter as a function of the departure from the designed ratio of output to input voltages; however, addition of a power-sensitive regulator compensates for variations in voltage, so giving the converter a constant power output.

2. The frequency of the interconnected systems and the changes in frequency of the systems are nullified by the conversion of alternating to direct current. Such a converter is a nonsynchronous tie between two systems. Further, all current transferred in the d-c link at the direct potential then existing is true power, without reactive currents.

3. Overexcited reactive current must be supplied by external sources.

4. The efficiency of the electronic converter is well maintained throughout its load range and is relatively higher than that of rotating frequency converters. Losses associated with the supply of overexcited kilovolt-amperes for commutation, and so forth, vary with each case and tend to increase the losses chargeable to the electronic converter.

5. When the necessary reactive kilovolt-amperes is supplied to the converter and its source is a synchronous machine, the electronic converter can supply power in multiple with synchronous generators or to an independent load and be subjected to frequency control at a fixed value or over a specified variable frequency range.

6. Fault control:

Internal Faults. Fault-suppression features, incorporated in the auxiliaries, make it possible to control excess power flow to individual internal-electronic equipment faults without resorting to the use of main circuit breakers.

Supply-Circuit Faults. No current can flow from the converter to a fault in the system supplying the rectifier.

Receiving-System Faults. Loads and faults on the receiving system cause the output regulators to tend to restore output to the preselected level. These regulators are extremely fast and therefore will tend to limit the converter output under receiving-system voltage depressions occurring at a rate slower than the speed at which the restoring controls can operate. The current that the inverter can supply to a fault in the receiving system is limited by the commutating ability of the converter at the particular margin in phase-angle setting established for the installation. For all practical purposes the current will rise to a maximum of 2.6 times normal load when the receiving-system voltage is appreciably depressed suddenly. Beyond this limit, the inverter will fail to commutate; this means it will contribute no current to a disturbance which causes a sudden depression in voltage below the limit of commutation.

Normal current can be restored, after the fault has been cleared, in a minimum of $^4/_{120}$ second and a maximum of $^4/_{15}$ second without the necessity for disconnecting the converter from the systems.

7. Weights, dimensions, and foundations of the equipment immediately involved in converting power from one system to another of different frequency are less for the electronic converter (excluding these elements contributed by the synchronous equipment required for commutation, and so forth) than for the rotating converter. Transformers are required by the electronic converter to insulate d-c from a-c systems and to obtain multiphase operation from a three-phase system. This addition generally can be expected to increase the weights and dimensions of the complete electronic converter above those for the rotating equipment. Foundation dimensions will vary with the particular location. Where both types of converters require transformers to raise potentials above those which can be withstood by rotating converters, these physical factors will be in favor of the electronic converter.

Table I summarizes for comparison the distinguishing characteristics of several types of system interconnecting means insofar as power flow, voltage and reactive current are concerned.

Explanation of Electronic-Converter Operation

Operation of power rectifiers is well understood, but inverted rectifiers are relatively new. By extending the principles of rectification, a quick grasp of inversion may be obtained. A simple underlying rule for both follows:

Providing adequate cathode electron emission is available and these electrons are not blocked by negative grids, only the anode or anodes most positive in the circuit will conduct current.

Refer to Figure 5 for illustration of the operating principles of an electronic converter. While the particular circuit shown in the illustration seldom would be used in practice, its operation is basically typical of practical converters and its simplicity facilitates explanation.

The circuit consists of two duplicate simple rectifiers, one designated *rectifier*, the other *inverter*. It may be assumed that the rectifier and inverter transformers are also duplicates and that the two a-c systems operate at the same voltage and frequency. The d-c link includes the electronic rectifiers, d-c interconnections, and d-c reactor. The d-c reactor may be regarded as an infinite inductance suppressing all ripple in the direct current for purposes of this discussion. Direct current, because of the rectifying property of both rectifier and inverter, always flows in the same direction through the electronic devices and d-c link, regardless of direction of power flow.

Principles of Rectifier Operation

A brief review of rectifier principles leads to the discussion of inverter principles. Figure 5B shows the succession of positive-half-wave voltages applied to the three electronic devices. For the ideal reactanceless case, current transfer would be instantaneous, and each rectifier

Table I. Distinguishing Characteristics of Various Methods of System Interconnection

	Electronic Converter	Copper-Conductor Interconnection	Variable-Ratio-Transformer Interconnection	Synchronous-Synchronous Converter	Induction-Induction Converter	Variable-Frequency-Ratio Converter (Scherbius Type)
Power flow is a function of	Voltage ratio	Voltage phase angle	Voltage phase angle	Voltage phase angle	Relative system frequencies	Converter variable frequency ratio
Power flow is controlled by	Adjusting phase-angle firing of converter	Prime-mover governor adjustment	Prime-mover governor adjustment	Prime-mover governor adjustment	Prime-mover governor adjustment	Adjusting slip of induction machine
Voltage is maintained by	Synchronous machines of both systems	Synchronous machines of either system	Synchronous machines of either system	Converter	Synchronous machines of both systems	Converter
Supplies overexcited current	No and cannot transfer it	No and can transfer it	Yes	Yes	No	Yes
Requires overexcited current	Yes	At high loads	At high loads	No	Yes	No
Responds to variations in relative system						
Frequencies	Insensitive	Yes	Yes	Yes	Yes	Compensated
Voltages	Compensated	Yes	Compensated	Compensated	Yes	Compensated
Can be used to control:						
Power flow	Yes	No	No	No	No	Yes
Reactive of systems	No	No	Yes	Yes	No	Yes

device in turn would conduct all the direct current after the intersection of its positive-half-wave voltage with the preceding positive half wave. Conduction periods are 120 electrical degrees, because each phase voltage is more positive than any other for such an interval.

Since practical circuits have reactance, current transfer or commutation cannot be instantaneous. During the commutating period, two phase voltages must remain equal, and direct voltage is equal to the average of the two phase voltages. Figure 5C shows the effect of this commutation on the direct output voltage. The average value of the direct voltage in Figure 5C is lowered because of the area above the notch lost during commutation. This notch increases in area in proportion to the amount of reactance and direct current, and therefore the direct-voltage–load characteristic of a rectifier droops.

When negatively energized grids are interposed between anodes and cathodes of mercury-arc rectifiers, or the instant of cathode emission is controlled, it is possible to prevent an anode from conducting current even when it becomes most positive. By properly timing the phase position of positive grid-excitation impulses, or the ignition of mercury-pool cathode spots with respect to applied anode voltage, current conduction can be controlled as desired. This action is called phase control and is illustrated in Figure 5D. The notches of lost voltage are larger than shown in Figure 5C, so the average direct voltage is controlled to a lower value. Many simple methods have been developed for smoothly adjusting the angle of phase control, and consequently the direct voltage.

PRINCIPLES OF INVERTER OPERATION

The choice of the word "inverter" to name the apparatus that changes d-c power to a-c power was apt, as an inverted similarity of the direct-voltage wave shapes will be noted in Figure 5E–G.

The d-c side of the inverter should be regarded as imposing a direct countervoltage to that of the rectifier. Consequently, for inverters the transformer neutral is the positive terminal and the cathode is the negative, as contrasted to the polarity for the rectifier (see Figure 5A).

Since inverters impose direct counter voltage, conduction of current in each phase occurs during the negative-half-wave crests (see Figure 5E). For the ideal reactanceless case and no phase control, instantaneous current commutation would be timed at the intersections of the successive negative half waves. The action of an inverter then is to chop d-c power into blocks, sort them out, and direct them into the correct transformer secondary winding at the right time to deliver a-c power to the system.

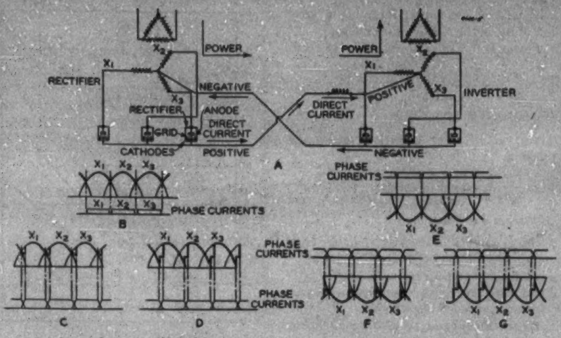
For inverter operation, commutation must be completed before the intersection of the negative half waves instead of after the intersection, as in rectification (see Figure 5F). If current were allowed to flow in any phase after its negative-half-wave voltage intersection with that of its successor, the former phase would steal back the current from its successor because the former has higher positive voltage. Such an event would constitute a commutation failure, and inversion would cease. Inverter direct countervoltage then would disappear quickly, causing the inverter to short-circuit the rectifier.

Since commutation occurs when the transformer phase voltages are negative and must be completed prior to the voltage intersections instead of later, phase control of conduction is mandatory for inverters. The angle of phase control for inverter operation, as compared to its position for rectifier operations, must be retarded to a suitable instant preceding the intersection of the negative half waves. Phase position of the angle for inversion is roughly 120–150 electrical degrees later than for full rectification voltage.

Generally, the angle of phase control for rectifiers is stated in terms of retard, zero angle meaning that conduction starts at the positive-half-wave intersections. Inverter angle of phase control is stated in terms of angle of advance, zero meaning that conduction starts at the negative-half-wave intersections.

When the angle of advance of inverter phase control is just sufficient for complete current commutation, the direct countervoltage will be as indicated by the heavy line in Figure 5F. Had the same angle of advance been chosen for a reactanceless circuit, permitting an assumption of instantaneous current transfer, the voltage loss would have been greater than that shown in Figure 5F. For such an hypothetical case and considering phases X_2 and X_3, all the area from the start of current conduction in phase X_2 to the intersection of the negative half waves X_1 and X_2 would have been lost. This loss would represent reduction in direct inverter countervoltage. Therefore, for inverter operation, the commutation reactance increases the direct voltage above the value which would be obtained for the same angle of advance with no reactance. For instance, the area between phase voltage X_2 and the heavy line tracing direct countervoltage during commutation in Figure 5F is additive direct countervoltage. Since this area increases with increased load, the d-c regulation curves for inverters rise with increasing load.

Reliable inverter operation demands successful commutation at all times. A



Principles of rectification

Principles of inversion

Figure 5. Operating principles of an electronic converter

Direct output voltage in B (heavy line) is envelope of successive a-c positive half-waves

Direct output voltage is heavy line in C and D

Direct countervoltage in E (heavy line) is envelope of successive a-c negative half-waves

Direct countervoltage is heavy line in F and G

more liberal angle of advance than illustrated in Figure 5F, providing a margin of angle for deionization and various other requirements which are explained in other parts of this and the companion papers, is desirable. Figure 5G illustrates the effect of selecting an angle of advance for inverter operation which has a large margin; that is, the alternating countervoltage of the inverter (heavy line) is reduced.

When the direct countervoltage is lowered, by advancing the angle of phase control, more power is transferred through the converter. Control of power flow through the converter is therefore responsive to phase control of either rectifier or inverter. Advancing the angle of the rectifier increases direct applied voltage and advancing the angle of inverter decreases direct countervoltage. Either causes more d-c power to flow.

Study of Figure 5G shows that the phase position of the current is ahead of the phase voltage. This means that a portion of the load on the receiving a-c system must consist of electrical apparatus that can accept current which leads voltage. Such apparatus as capacitors or overexcited synchronous machinery are the only types that fulfill this requirement. Therefore, a requirement to the use of electronic converters is that the supply and receiving a-c systems must supply leading reactive volt-amperes not only to their own inductive loads, but also to the converter.

Another requirement of the receiving a-c system is that it must provide a pattern of alternating voltage. Study of the wave forms for inversion, Figure 5E–G, shows that phase voltages must be applied to commutate the current. Such alternating voltage may be provided by either an a-c generator, or an overexcited synchronous machine floating on the receiving a-c system. The inverter, which is the subject of this paper, was not specified to furnish power to a load independent of prime movers. An electronic converter may be adapted to stub-end feed, but a converter for this purposed would include modifications to the design herein described.

Power Flow in Either Direction

Providing the foregoing requirements for a receiving system are met, it is possible to transfer power in either direction. If both electronic elements are identical, reversal of action of the unit designated rectifier to that of an inverter and inverter to that of a rectifier (Figure 5A) is brought about by readjusting the angles of phase control to proper positions. This adjustment can be made instantly, when desired, by throwover contactors in the control circuit.

Operating Characteristics

Electronic converters have operating characteristics peculiarly different from other conventional apparatus, and these characteristics should be well understood by application and operating engineers.

Wave Shape

It is well known that mercury-arc power rectifiers produce harmonics in the a-c and d-c systems. The number and magnitude of these harmonics depend upon the number of phases, reactance, load, and amount of phase control. Harmonics and wave distortion for inverters are the same as for rectifiers and depend upon the same factors. Treatment of the problem is well understood for rectifiers, and in general the same treatment applies for inverters. Wave distortion can be reduced by means of phase multiplication, proper co-ordination with the a-c systems, or addition of filters. It is beyond the scope of this paper to discuss the details of such treatment as they are covered fully in other literature.[3] Wave shapes obtained on the high-voltage sides of 60- and 25-cycle transformers with power flowing from the 60- to the 25-cycle systems are shown in the oscillograms of Figure 6.

Direct-Voltage Regulation

Figure 7 shows the direct-voltage-regulation characteristics of a rectifier and inverter combination when the rectifier and inverter transformers have the same secondary voltages and reactances. In the right quadrant is plotted the typical drooping rectifier-regulation characteristic, and in the left, the rising inverter curve. Similarity of these curves to those of d-c generators and motors, respectively, may be noted.

The curves drawn through 100 per cent direct voltage represent the maximum voltage level obtainable with zero-angle phase control. Such high direct countervoltage can be obtained for the

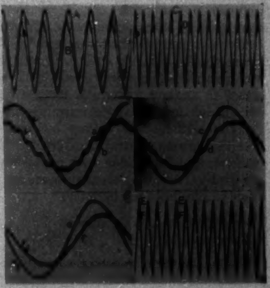


Figure 6. Voltage and current oscillograms on high-voltage side of electronic converter operating with power flowing from 60-cycle to 25-cycle system

A—25-cycle voltage, one inverter operating 12 phase

B—25-cycle current, one inverter operating 12 phase

C—60-cycle voltage, one rectifier operating 12 phase

D—60-cycle current, one rectifier operating 12 phase

E—60-cycle voltage, two rectifiers operating 24 phase

F—60-cycle current, two rectifiers operating 24 phase

a, b, c, d, e, f, designate the same waves as *A, B, C, D, E, F,* except that the latter were taken with a slower film speed

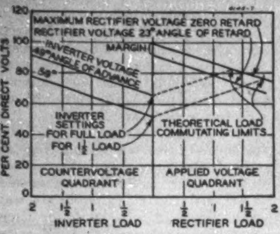


Figure 7. Direct-voltage regulation characteristics of rectifier and inverter

inverter in theory only. Successful commutation of a practical inverter requires an appreciably advanced angle of phase control. Also the inverter direct countervoltage must tend to be lower than the rectifier voltage to cause d-c power to flow.

If inverter-regulation curves corresponding to angle of phase control are replotted in the rectifier quadrant, the point where they intersect the 100 per cent rectifier-voltage curve determines the load at which inverter commutation theoretically fails because of insufficient angle of advance. Practically, the upper limit is appreciably lower because a margin angle for deionization is required. If the direct-voltage level of the rectifier is reduced by its phase control or by a change in its transformer ratio, inverter countervoltage must be lowered and consequently the inverter angle advanced to maintain the same power flow. This increase in angle represents a margin for deionization and successful commutation. Reduction of rectifier voltage by means of either phase control or a change in transformer turn ratio or both commonly are used to force the necessary operating-angle margin upon the inverter.

Power Factor and Reactive Requirements

There are three reactive components which cause the inherently lagging power factors of both rectifiers and inverters. The most important is the displacement of current phase position from voltage position caused by the combination of commutation and phase control. Harmonics produced by rectifiers and inverters also increase the kilovolt-amperes of the systems. The extra heating effect of harmonics should be weighted and included in over-all power-factor calculations. There are many rectifier circuits, and each has its own characteristic harmonics.[4] The third reactive contribution which lowers power factor is the magnetizing requirement for the main transformers. In the following discussion of power factor and reactive requirements, this last factor is not included as it is usually negligible above one-quarter load. Transformer-magnetizing current gradually reduces the power factor to a very low value in the region near zero load.

Power factor of a rectifier has the form

$$\mathrm{Pf} = \frac{K}{\sqrt{1-CF_1(\mu\alpha)}}\left(\cos\alpha - \frac{E_x}{E_{do}}\right)$$

(Equation 3 of Appendix.)

The inverter power factor is given by the relation

$$\mathrm{Pf} = \frac{K}{\sqrt{1-CF_3(\mu\beta)}}\left(\cos\beta + \frac{E_x}{E_{do}}\right)$$

(Equation 4 of appendix.)

K and C, as usual in these relations, are constants for the particular rectifier circuit.

μ is the commutating angle.

α and β are the angles of rectifier retard and inverter advance respectively.

E_x is the voltage change due to commutating reactance.

E_{do} is the theoretical direct voltage.

F_1 and F_3 are mathematical expressions.

The relations given in equations 3 and 4 are derived in the appendix. If similar rectifier and inverter circuits and transformer constants and equal voltage levels are assumed for the rectifier and inverter systems, it may be shown (see appendix) that

$$\mathrm{Pf}_{(\mathrm{inverter})} = \mathrm{pf}_{(\mathrm{rectifier})} - \frac{K}{\sqrt{1-CF_3(\mu\alpha)}} \times (\text{per cent conversion loss})$$

(Equation 14 of appendix.)

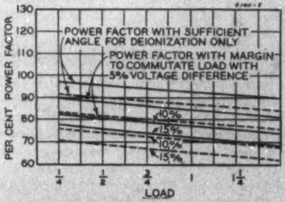


Figure 8. Power factor of a typical electronic converter having identical 12-phase transformers and losses of 5.5 to 6.8 per cent

Duplicate transformers each: 12-phase double-way, 15 per cent reactance assumed, angle for deionization 5 degrees

——Rectifier ----Inverter

Equation 14 means that providing the previous conditions of similarity for rectifier and inverter operation are met, a good rule of thumb for estimating power factor is:

The power factor of the inverter is lower than the power factor of the rectifier by approximately the per cent of the conversion losses.

It will be noted that the frequency parameter did not enter the foregoing expressions. Therefore, again providing the same conditions of similarity are met, differences in system frequencies do not affect power factor.

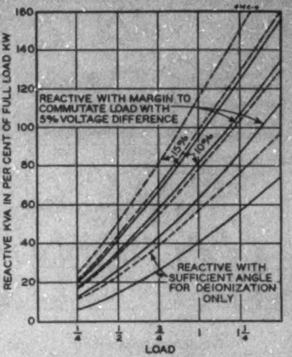


Figure 9. Reactive kilovolt-amperes of a typical electronic converter having identical 12-phase transformers and losses of 5.8 to 6.8 per cent

Duplicate transformers each: 12-phase double-way, 15 per cent reactance assumed, angle for deionization 5 per cent

——Rectifier ----Inverter

Power-factor and reactive-kilovolt-ampere characteristics of a typical converter having identical 12-phase transformers and losses of 5.5 to 6.8 per cent (depending on angle of phase control) are illustrated by Figures 8 and 9, respectively. Power factor and reactive kilovolt-amperes are shown for four different operating conditions.

Optimum power factor, when both varying voltage ratio and reversals of power flow must be provided for, may be obtained by selecting suitable transformer voltage ratios and then using phase control as a refinement for any single turn ratio. When control by transformer ratio is not used and phase control is the sole means, sufficient total angle for three separate requirements must be available, namely:

1. Sufficient angle for commutation of current, plus

2. Sufficient angle for tube deionization, plus

3. Margin angle for expected transient voltage departures from normal on the two a-c systems which would cause corresponding load changes

Margin angle for transient voltage changes, having been provided, phase control will compensate for normal voltage changes.

Efficiencies

Per cent losses increase when converters operate with lower power factor. With lower power factor, the direct voltage is proportionately lower.[5] For the same kilowatt output at lower voltage, the current is increased and some of the transformer losses increase in proportion to the square of the current. The effects of these extra losses on inverter power factor were included in the results illustrated in Figures 8 and 9.

Losses for the electronic portion of such converters depend somewhat upon the direct-voltage level selected. Per cent arc loss in the rectifiers is expressed as (E_a/E_{do}) 100, where E_a is the arc-loss voltage and E_{do} is the theoretical direct voltage. Therefore, increasing the direct-voltage level decreases the per cent losses. Direct-voltage levels sufficiently high to keep the total arc losses less than one per cent may be selected using available power tubes.

Figure 10 illustrates the efficiency of a typical electronic power converter where the direct-voltage level is selected high enough to keep arc losses approximately one per cent. Higher efficiency, as compared to other forms of conversion, is a distinguishing characteristic of this type converter. The illustration shows that for the lower power factors, where the inverter is set for a large margin angle to commutate load fluctuation produced by voltage variations, the efficiency is reduced over one per cent.

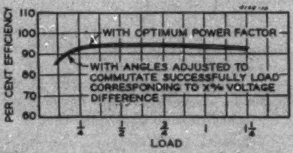


Figure 10. Efficiency of a typical electronic converter having a d-c level corresponding to one per cent arc loss

Control of Electronic Disturbances

There are certain internal fault conditions associated with rectifiers and inverters which produce high current in the circuits. Arc-backs[6] are the characteristic type of internal fault of the rectifier. Commutation failures caused by "shoot-through" (failure of grids to prevent conduction during positive half waves) or lack of sufficient inverter angle sometimes happen and are the characteristic types of faults of the inverter circuit. Both rectifier arc-backs and inverter commutation failures cause high currents in the rectifier circuits and a-c supply lines feeding the rectifier. Inverter commutation failures, because they short-circuit the rectifier, produce high direct current in the d-c link.

Control of both kinds of disturbances should be by means of ignition and grid excitation in the rectifier tubes. High-speed relays and contactors may be closed instantly when the high current is detected. The contactors stop ignition and bias the grids off in the rectifier. Such action is known as arc suppression[7] and reduces the rectifier alternating and direct currents to zero in a small fraction of a second, whereupon the suppression is removed and operation is resumed immediately.

Table II. Major Apparatus of One 10,000-Kw Electronic Converter

	Frequency, Cycles	
	25	60
Nominal circuit voltage	44,000	69,000
Insulation level	69,000	115,000
Oil circuit breaker		
Type	*FK*339-1,000	*FK*-339-1,500
Volts	69,000	115,000
Amperes	600	1,200
Interrupting megavolt-amperes	1,000	1,500
Power transformer		
Type	*HTD*	*HTD*
Kilovolt-amperes, continuous (45 C)	13,120	13,120
Kilovolt-amperes, 2 hours	16,400	16,400
Kilovolt-amperes, 1 minute	26,240	26,240
Voltage, high	44,000	69,000
Voltage, low	12,970	12,970
Connections, high voltage	Delta and wye	Delta and wye
Connections, low voltage	Zigzag	Zigzag
Taps, high voltage		
Above normal, per cent	2–5	1–2½
Below normal, per cent	2–5	3–2½
Electronic converter		
Type	6-*RP*-42A1	6-*RP*-42A1
Kilowatts, continuous	10,000	10,000
Kilowatts, 2 hours	12,500	12,500
Kilowatts, 1 minute	20,000	20,000
Voltage, alternating	12,970	12,970
Voltage, direct	29,400	29,400
Amperes, continuous, direct	340	340
Tubes, designation	*GL*-506	*GL*-506
Tubes, type	Pentode ignitron	Pentode ignitron
Number	12	12
Amperes, average	150	150
Peak inverse voltage	20,000	20,000
D-c reactor		
Type	*Y*-3426A	
Form	*C*	
Volts	12,730	
Amperes, continuous	420	
Amperes, 2 hours (60 C)	553	
Heat exchanger		
Type	Water to water	
95 F max water required at 100 per cent converter load	45 gpm	
Auxiliary power transformer		
Type	*HT*	*HT*
Kilovolt-amperes (55 C)	100	150
Voltage, high	44,000	69,000
Voltage, low	240/480	480
Panels and control equipment for excitation, regulation, switching, metering, and relaying.		

This action is so fast that it is not necessary to disconnect the converter and restart it in service later as is common with conventional low-voltage power rectifiers.

Effect of External Faults

The behavior of electronic converters, when there are faults on the two a-c systems to which they are connected, is peculiarly different from that of other types of converters. When disturbances occurring in the supplying a-c system produce voltage dips, the dips cause reduction in power flow to the receiving system. The electronic tubes are unidirectional-current devices. The direct current will not reverse, and, even if the supply system voltage falls to zero, the converter will not reverse direction and feed power to the fault. Power flow can reverse only when the phase control is intentionally readjusted.

When the receiving-system voltage drops because of system faults, but not enough to cause inverter commutation failure, there is a moderate increase in power output. For instance, if the voltage reduction did not exceed five per cent and the direct voltage regulation of the rectifier and inverter were ten per cent, the load would be increased 50 per cent if the converter were carrying full load. A watt regulator restores the previous load within a few seconds, even if the lower

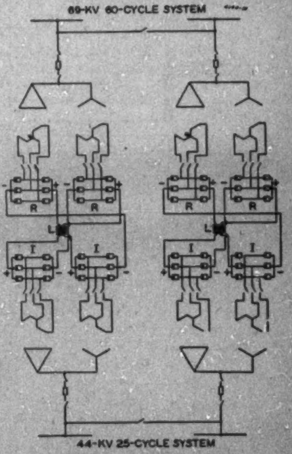


Figure 11. Main power diagram of the two 10,000-kw electronic converters

R—Rectifier
I—Inverter
L—D-c reactor

Table III. Area, Dimensions, and Weights of Indoor Equipment of Two 10,000-Kw Electronic Converters

	Square Feet	Pounds
Floor area devoted to outdoor high-voltage gear, transformers, and d-c reactors	15,160	
Floor area devoted to control	3,024	65,000
Electronic converters	3,024	135,000
Pounds per kilowatt of		
Control		3.25
Electronic converter		6.75

voltage persists. If, however, receiving-system disturbances reduce the voltage so much that the increased load causes commutation failure, delivery of power from the converter will stop. As previously stated, the maximum current supplied by the inverter to the receiving system is about 2.6 times normal. In so doing, they of themselves do not assist in maintaining system voltages for heavy load changes or disturbances; yet the synchronous capacity required for their excitation does tend to support system voltage. Provided system faults are cleared by circuit breakers within one to three seconds, the converter will fully protect itself by means of its own internal arc-suppression control and its power circuit breakers may remain closed. Arc-suppression control is backed up by protective relays which have time delay. Therefore, the converter need only be disconnected when serious disturbances persist.

Selection of Circuits and Electronic Devices

There are two kinds of rectifiers of suitable rating and characteristics and many circuits which may be selected for such converters. Either conventional pumped rectifiers or sealed rectifiers may be used. The pumped type in its present form, however, has some disadvantages. For equipment ratings of the order of those discussed in this paper, the operating-voltage levels of available pumped rectifiers are lower than those of sealed rectifiers, with consequent lower efficiency. Economy of vacuum pumps dictates that as much rectifier capacity as possible be connected to a single exhaust system. This requirement tends to limit the selection of rectifier circuits for pumped rectifiers to single-way (half-wave) types.

The simplest and most economical circuits from the standpoint of transformer kilovolt-ampere utilization, are double-way (full-wave) combinations. Sealed single-anode rectifiers are suited ideally to the latter circuits. A 200-ampere 20,000-volt stainless-steel water-cooled pentode ignitron tube is described in a companion paper.[8] Selection of sealed rectifiers also eliminates occasional complicated repairs to rectifiers on the user's premises. If a sealed rectifier ever is involved in trouble, the user simply replaces it and returns it to the factory where suitable factory conditions and expert personnel are available for rework. When the pumped type is in trouble, local repairs sometimes involve long service outages and often must be made under undesirable atmospheric conditions. Experience with tube life has been so satisfactory that choice of sealed single-anode metal rectifiers good for operating voltages as high as 15,000–20,000 volts is sound.

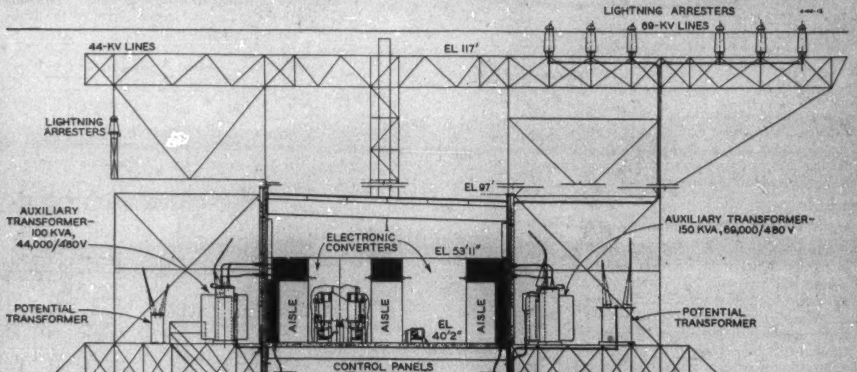
Table IV. Results of Operation

	Unit 1	Unit
Kilowatt-hours produced	18,220,000	5,430,000
Kilowatts carried, maximum		
1 minute	20,000	20,000
2 hours	12,500	12,500
2 weeks	12,500	
Load factor based on continuous rating, per cent	68	48

Description of the 20,000-Kw Electronic Power Converter

An electronic converter rated 20,000 kw, which interconnects the Carnegie-Illinois Steel Corporation's 69,000-volt

Figure 12. Cross-section elevation of the 20,000-kw electronic-converter station at the Carnegie-Illinois Steel Corporation


LIGHTNING ARRESTERS
69-KV LINES
44-KV LINES
EL 117'
LIGHTNING ARRESTERS
EL 97'
AUXILIARY TRANSFORMER-100 KVA, 44,000/480V
ELECTRONIC CONVERTERS
EL 53'11"
AUXILIARY TRANSFORMER-150 KVA, 69,000/480 V
POTENTIAL TRANSFORMER
AISLE
AISLE
AISLE
EL 40'2"
POTENTIAL TRANSFORMER
CONTROL PANELS
EL 27'6"
ANODE DISCONNECTING SWITCHES
OIL CIRCUIT BREAKER
POWER TRANSFORMERS
13,120 KVA 44/12.97 KV
13,120 KVA 69/12.97 KV
OIL CIRCUIT BREAKER
EL 0'0"
44-KV 25-CYCLE SIDE
69-KV 60-CYCLE SIDE

60-cycle system and its 44,000-volt 25-cycle system, was placed in service during December 1943. It is designed for controlling any desired constant flow of power within its rating in either direction. Specified for industrial service, it is capable of carrying 125 per cent load for two hours and 200 per cent load for one minute.

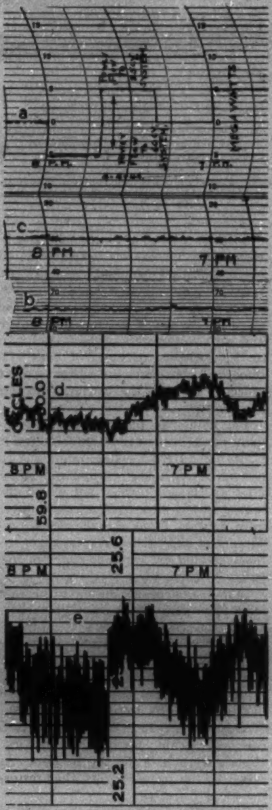


Figure 13. Charts illustrating effects of manual resetting of automatic constant-power control including power reversal

All charts taken between 7:00 and 8:00 p.m., April 4, 1944

(*a*)—Changes in load by manually adjusting kilowatt regulator for constant load to 25-cycle system, zero load, and reversing direction to supply load to 60-cycle system

(*b*)—69-kv 60-cycle system voltage

(*c*)—44-kv 25-cycle system voltage

(*d*)—69-kv 60-cycle system frequency

(*e*)—44-kv 25-cycle system frequency

MAIN POWER-CIRCUIT DIAGRAM

Figure 11 shows the main power diagram. The 20,000-kw installation is composed of two 10,000-kw identical units. The units may be operated in parallel, or when power requirements are within the capacity of one 10,000-kw set either may be operated singly.

Both the 60- and 25-cyle transformers are alike in design, except for frequency and primary voltage. Transformer connections for each 10,000-kw unit are arranged for 12-phase operation. The secondary windings are zigzagged for a phase displacement of $\pm 7^1/_2$ electrical degrees. One 10,000-kw unit is internally connected for $+7^1/_2$ degrees and the other for $-7^1/_2$ degrees. Therefore, when both units are in service, the combination operates as a 24-phase unit. This phase multiplication reduces wave distortion and prevents interference with communication circuits.

The single-anode pentode ignitron tubes are connected in eight three-phase double-way (full-wave) six-tube circuits. Rectifiers and inverters of each 10,000-kw unit are staggered in the series d-c link. When one of the neutrals of the transformer wye is grounded, this staggered arrangement of rectifiers and inverters reduces insulation-to-ground requirements as compared with connecting two rectifiers in series and then the two inverters in series. If it had not been desirable for operating reasons to have two separate individually operable 10,000-kw units, all eight six-tube groups could have been connected in series in one d-c link. Such connections would compel operation as a single 20,000-kw unit but would point the way to increased kilowatt ratings, using multiplication of the same elements of apparatus employed in this typical installation.

It will be noted that there are no power circuit breakers in the d-c circuit. The a-c circuit breakers were the only ones required.

PHYSICAL ARRANGEMENT

Figure 12 shows a cross section of the complete 20,000-kw converter installation. Converter and control equipment are housed in a windowless air-conditioned building, the first floor of which is located above the main transformers. Ordinary steel columns support the weight of the building, all indoor apparatus, and some of the high-voltage gear. This arrangement was used because the location is in a flood area and because ground space was extremely restricted. The main transformers are mounted with their bases at ground level. Accessories such as thermometers and liquid-level gauges, were installed high enough on the transformers to be above flood level. The light weight of the electronic equipment proved an advantage in this installation. Its adaptability to mounting on ordinary flat floor surfaces also simplified and reduced installation costs as compared to other available converter equipments. These features may be regarded as additional distinguishing characteristics of electronic-type converters.

COOLING REQUIREMENTS

All the apparatus except the electronic tubes is cooled by natural ventilation. The tubes have water jackets, and their losses are removed by recirculating water. Each 10,000-kw unit has a complete recirculating cooling system, including suitable motor-driven water-circulating pumps and water-to-water heat exchangers. The two heat exchangers are grounded and either distilled or treated distilled water may be used as a recirculating coolant. Raw water from the

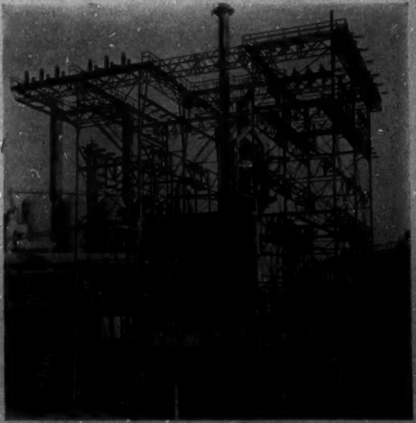


Figure 14. Electronic converter and control installed in brick housing

60-cycle switching and lines at left, and 25-cycle switching and lines at right of structure plant water system passes through the heat exchanger in proper quantity, controlled by thermostatically operated water valves, to remove the losses and hold the proper temperature.

There are no windows in the building. Ventilation is provided by a motor-driven blower which passes incoming air through a dust filter.

Supervision and Control

This installation was designed for unattended operation. It is located near a power station where electrical operators are on duty. Two duplicate operator's panels were provided, one located in the converter building and the other in the power station. The panel at the converter station has permissive switches which transfer complete functional control to the power station when desirable. An operator in the power station can perform the following operations:

1. Open and close all four power circuit breakers.
2. Select direction of power flow.
3. Place either or both 10,000-kw units in operation.
4. Select one of three types of operation:
   - *A*. Manual control of desired power flow.
   - *B*. Automatic constant-watt control of power flow.
   - *C*. Control of power through the converter tending to hold constant rate of purchased 60-cycle power.
5. Adjust magnitude of power transferred using either type-*A* or type-*B* control.
6. Observe certain abnormal conditions existing at the converter station by means of annunciators and alarms.
7. Remove either or both units from service.

It is necessary for an operator to go to the converter station to prepare the equipment for service after a complete shutdown. Control for converter auxiliaries is located at the building. Operation of high-voltage disconnecting switches must be done at the station. Anode-line disconnecting switches, located on the first floor of the converter building, must be operated manually.

All indoor control equipment, including the local operator's panel, is located on the first floor. The only apparatus on the second floor are the four large tube enclosures, each housing 12 tubes, and an enclosure where three spare tubes may be stored.

One section of the local operating panel mounts a cathode-ray oscillograph and transient indicators. These test instruments are desirable when it is necessary to locate internal faults or pick out any tube which may not be operating properly.

Figure 15. Converter-tube-assembly enclosures for rectifiers-inverters—view through center aisle

Results of Operation

The first 10,000-kw unit was energized initially on December 6, 1943, and the second unit on February 15, 1944. The results of operation are given in Table IV for the period after trial runs were completed until April 4, 1944. Except during trial periods, normal operation of both converters has been directed by the Carnegie-Illinois Steel Corporation load dispatcher who designated constant power flow under automatic control from the 60- to the 25-cycle system and at various loadings, dependent upon system requirements.

Charts taken on conventional curve-drawing instruments are reproduced in Figure 13. By comparing the various charts it will be noted that when the load to the 25-cycle system was reduced to zero and reversed to the 60-cycle system, at 7:40 p.m., the 25-cycle-system frequency fell off and then steadied as the well-loaded prime movers of that system picked up load. The change in loading on the 60-cycle system due to this operation was so small in relation to other circumstances that the 60-cycle frequency and voltage do not appear to have been affected. There is, however, a slight rise in 25-cycle voltage noted at about eight p.m. which may be attributed to the reduction in 25-cycle system loading through the converter at that time. Of particular note is the constancy of the load through the converter in relation to the changes taking place in the frequency and voltage ratios of the two systems.

General Applications for the Electronic Converter

In summary, it may be said that having the characteristics mentioned and one or more of the controls which fit the case, the electronic converter is suitable for:

A. Controlling power between systems of like frequency.

B. Converting power from a system of one frequency to that of another frequency.

C. Furnishing power to an independent load at like or dissimilar frequency to that of the source system.

D. Producing direct current for transmission from the source rectifier to the output inverter.

In these several applications the control of power flow may be concentrated at one point, is exceedingly simple, and is readily amenable to a variety of superimposed response systems. With the converter under constant-load control and with voltage regulators in operation on synchronous machines of both systems, load changes on one system are not reflected through the converter to the other system.

The ability of the converter to suppress excess current to internal electronic faults and external system faults and to restore power under normal conditions after an elapsed time of not over four-tenths second after fault release reduces fault damage, duty, and the stresses on equipment and circuit breakers below extents created by corresponding-size rotating equipment.

Certain systems are subjected to wide load variations having repetitive characteristics. Some of these systems maintain their frequency within relatively close limits while others allow a wider variation. Whether this wider variation is intentional or not, the result is a release of flywheel inertia energy during load increases, which reduces the rates at which fuel and steam supply are increased. Upon release of load, the frequency is raised by the prime movers, thus restor-

Figure 16. Unit heat exchanger for two 10,000-kw converters

ing energy to the flywheel. The electronic converter is especially adapted to interconnection of such systems with others of more rigid frequency since the margin in overload and pull-out torque normally required in rotating converters to absorb frequency variations in the interconnected systems is not required.

Appendix. Expressions for Rectifier and Inverter Power Factor

The power factor of a rectifier takes the following form:[8]

$$\text{Pf} = K\frac{\dfrac{E_d}{E_{d0}}}{\sqrt{1-CF_1(\mu\alpha)}} \qquad (1)$$

Constants K and C are the characteristics of the type of circuit used.

$F_1(\mu\alpha)$ is function of angle of commutation and angle of retard.

E_d/E_{d0} is the ratio of rectifier direct voltage at load to its no-load direct voltage without retarded phase control.

α is angle of retard of phase control.

$$\frac{E_d}{E_{d0}} = \cos\alpha - \frac{E_x}{E_{d0}} \qquad (2)$$

where E_x is direct volts regulation due to reactance.

$$\therefore \text{pf} = \frac{K}{\sqrt{1-CF_1(\mu\alpha)}}\cdot\left(\cos\alpha - \frac{E_x}{E_{d0}}\right) \qquad (3)$$

Similarly the inverter power factor may be expressed in the same form.[8]

$$\text{Pf} = \frac{K}{\sqrt{1-CF_2(\mu\beta)}}\cdot\left(\cos\beta + \frac{E_x}{E_{d0}}\right) \qquad (4)$$

where β is angle of advance of phase control and $F_2(\mu\beta)$ is function of angle of commutation and angle of advance.

The direct voltages of the rectifier and inverter must be equal for any load as they are connected together and electrically oppose each other in the same circuit. Expressions for direct voltage of rectifiers and inverters have the following form:[4]

$$E_d(\text{rectifier}) = \left(\sqrt{2}E\frac{P}{\pi}\sin\frac{\pi}{P}\right)\times \cos\alpha - \frac{1XP}{2\pi} - E_{arc} - \frac{W_T}{I} \qquad (5)$$

where:

E_d = actual direct voltage

E = transformer-secondary volts, anode neutral (rms)

$P = \dfrac{360}{\text{degrees conduction} - \text{degrees commutation}}$ = number phases per rectifier

I = load current commutated

X = commutating reactance

E_{arc} = rectifier arc drop

W_T = transformer resistance loss

and

$$E_d(\text{inverter}) = \left(\sqrt{2}E\frac{P}{\pi}\sin\frac{\pi}{P}\right)\times \cos\beta + \frac{1XP}{2\pi} + E_{arc} + \frac{W_T}{I} \qquad (6)$$

$$\sqrt{2}E\frac{P}{\pi}\sin\frac{\pi}{P} = E_{d0} \text{ (for both rectifier and inverter)} \qquad (7)$$

since $E_d(\text{rectifier}) = E_d(\text{inverter})$

$$E_{d0}\cos\alpha - \frac{1XP}{2\pi} - E_{arc} - \frac{W_T}{I} = E_{d0}\cos\beta + \frac{1XP}{2\pi} + E_{arc} + \frac{W_T}{I} \qquad (8)$$

if it is assumed that the converters are identical, that is, same type tubes, transformer reactances, losses, secondary voltages, and that the alternating voltage levels of the two systems are normal.

From this expression

$$\cos\beta = \cos\alpha - \frac{PIX}{\pi E_{d0}} - 2\frac{E_{arc}}{E_{d0}} + \frac{W_T}{E_{d0}I} \qquad (9)$$

$$\frac{PIX}{\pi E_{d0}} = 2\frac{E_x}{E_{d0}}$$

$$\cos\beta = \cos\alpha - 2\frac{E_x}{E_{d0}} - 2\left[\frac{E_{arc}}{E_{d0}} + \frac{W_T}{E_{d0}I}\right] \qquad (10)$$

From equation 4

$$\cos\beta = \frac{\text{pf (inverter)} - \dfrac{K}{\sqrt{1-CF_2(\mu\beta)}}\cdot\dfrac{E_x}{E_{d0}}}{\dfrac{K}{\sqrt{1-CF_2(\mu\beta)}}}$$

$$\text{Pf (inverter)} = \frac{K}{\sqrt{1-CF_2(\mu\beta)}}\times \left(\cos\alpha - \frac{E_x}{E_{d0}}\right) - \frac{2K}{\sqrt{1-CF_2(\mu\beta)}}\times \left[\frac{E_{arc}}{E_{d0}} + \frac{W_T}{E_{d0}I}\right] \qquad (11)$$

$$\frac{K}{1-CF_2(\mu\beta)} = \frac{K}{1-CF_1(\mu\alpha)} \text{ (approximately)}^{8} \qquad (12)$$

$$2\left[\frac{E_{arc}}{E_{d0}} + \frac{W_T}{E_{d0}I}\right] = \text{per cent loss of converter} \qquad (13)$$

By substitution in equations 3, 12, and 13

$$\text{Pf (inverter)} = \text{pf (rectifier)} - \frac{K}{\sqrt{1-CF_1(\mu\beta)}} \text{ (per cent conversion loss)} \qquad (14)$$

This means that providing the foregoing conditions of similarity for rectifier and inverter are met, a good rule of thumb for estimating power factor is: Power factor of inverter is lower than power factor of rectifier by the amount of the per cent of the converter transformer and electronic losses.

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Design of an Electronic Frequency Changer

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THIS paper discusses the circuit arrangement and design features of an electronic-frequency-changer station for interchanging power between the Carnegie-Illinois Steel Corporation's 25-cycle system and their 60-cycle system. The transfer of power by rectification and inversion has been discussed in a companion paper by Cramer, Morton and Darling.[1] The reader should consult their paper to obtain a clear picture of the power flow from alternating to direct current in the rectifier and from direct to alternating current in the inverter. The operating requirements as presented in the Cramer, Morton and Darling paper may be summarized as follows:

1. The direction of power flow must be reversible at will.
2. The load must be independent of system frequency and small variations in system voltage.
3. It must be possible to adjust the load to any desired value for either direction of power flow.

In addition to the operating control, the frequency changer must be able to clear faults in the electronic tubes rapidly and without breaker operation to give a minimum power disturbance.

The frequency-changer station herein described has a continuous capacity of 20,000 kw in two similar units of 10,000 kw each. Operation at 125 per cent load for one hour and 200 per cent load for one minute is permitted. The single-line circuit diagram in Figure 1 shows the power circuit for one 10,000-kw unit.

Power Transformers

The power transformers are required for each 10,000-kw unit as shown at T_1 and T_2 of Figure 1. T_1 is connected to the 44-kv 25-cycle system, and T_2 is connected to the 69-kv 60-cycle system. These rectifier–inverter transformers each are rated 13,120 kva 45 degree centigrade winding rise, continuous, and are suitable for 125 per cent load for two hours and 200 per cent load for one minute. The 25-cycle transformer is inherently larger in size than the 60-cycle unit; otherwise, the general construction and electrical characteristics of the two machines are the same. The 60-cycle transformer is provided with four $2^{1}/_{2}$ per cent primary-voltage taps, and the 25-cycle unit with four five per cent primary-voltage taps, brought to ratio adjusters for no-load operation. The secondary winding of each transformer is rated 12,970 volts phase-to-phase.

These transformers are designed to give 12-phase operation for each 10,000-kw frequency-changer unit and an over-all 24-phase relationship for the complete 20,000-kw installation. Since double-way rectification is used, a six-phase anode firing relationship is obtained from each three-phase secondary winding. Therefore, a 12-phase wave form can be obtained by using two three-phase secondary windings displaced from each other by 30 degrees. In this installation the 30-degree displacement between the secondary windings is obtained by using two three-phase cores (see Figure 2) for each 13,120-kva unit with the primary winding connected in delta on one of the cores and in wye on the other. The delta and wye primary windings are connected in parallel inside the transformer case, and the line leads are brought out through a single set of high-voltage cover bushings.

A 15-degree phase displacement is required between the two 10,000-kw frequency-changer sets in order to obtain an over-all 24-phase relationship from the 20,000-kw installation. This is obtained by using a three-phase zigzag secondary winding on each transformer core which can be connected for either $7^{1}/_{2}$ degrees leading or $7^{1}/_{2}$ degrees lagging phase shift by means of a terminal board inside the transformer case. By connecting all of the transformer windings on one of the 10,000-kv sets $7^{1}/_{2}$ degrees leading, and the windings on the other set $7^{1}/_{2}$ degrees lagging, an over-all 24-phase relationship is obtained.

Oversized bushings are used on the transformers because of atmospheric conditions around a steel mill. The primary windings are insulated to correspond to rated line voltage and are provided with electrostatic shielding at the line ends to protect against voltage surges on the power system. The insulation level of the secondary windings is coordinated with that of the frequency changer. The continuous disk type of winding is used for both the primary and secondary coils. This type of winding especially is suited for this application, as it can be adequately insulated and it has exceptional mechanical strength.

Current-Limiting Reactors

The reactors shown in the a-c lines of Figure 3 are provided to limit the fault currents during arc-back or a short circuit on the d-c bus. Two of these three-phase air-core reactors are mounted inside each transformer case, one for each three-phase secondary winding. Each three-phase reactor has four windings arranged in a vertical stack, the two middle windings being used for two of the phases, with the top and bottom windings connected in series to constitute the third phase. This arrangement is used to obtain balanced mutual reactance among all three phases. Each reactor is enclosed in a copper flux shield (see Figure 2) to confine the flux and thus prevent stray heating and nonlinear-reactance characteristics due to adjacent magnetic material.

The rectifier–inverter transformers are located outdoors, beneath the structure housing the electronic-frequency-changer

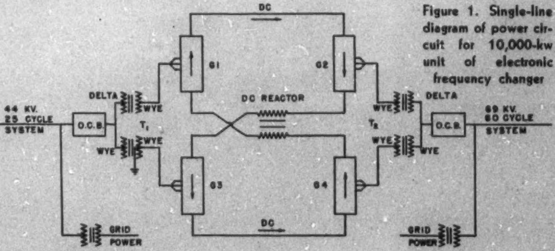

Figure 1. Single-line diagram of power circuit for 10,000-kw unit of electronic frequency changer

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The authors acknowledge the co-operation and assistance of the many individuals of General Electric Company who have contributed to the design and manufacture of this electronic frequency changer. E. F. W. Alexanderson and the late C. W. Stone were largely responsible for the early progress in power-tube applications.

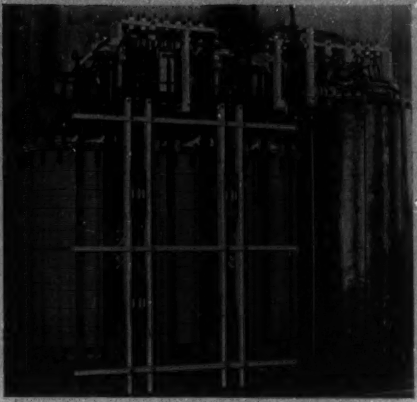


Figure 2. Assembled core and coils for oil-immersed shielded-winding mercury-arc rectifier–inverter transformer with shielded reactor, 44,000 to 12,970 zigzag volts, 25 cycles

For use with 10,000-kw 15,000-volt d-c electronic frequency changer. High-voltage-side view. For the Carnegie-Illinois Steel Corporation

equipment and control apparatus. Limitations in the amount of floor space and height available for the transformers and a-c reactors made it imperative that the over-all dimensions be kept to a minimum, consistent with the ratings of the apparatus. It was found that by mounting the a-c reactors inside the transformer case, instead of in a separate tank which is the usual practice, a considerable saving could be made in over-all floor space, number of cover bushing, and number of external interconnections required for the installation. Each of the 13,120-kva 25-cycle transformers with built-in a-c reactors for this installation has an over-all floor space of $17^1/_2$ feet by $23^1/_2$ feet, and an over-all height of 20 feet, and weighs approximately 280,000 pounds complete with oil.

D-C Reactors

One double-winding d-c reactor is used for each 10,000-kw frequency changer. Each winding is connected in series with the d-c bus between a rectifier and inverter unit as shown in Figure 1. The reactors are oil-immersed, assembled in a rectangular tank with the four line leads brought out through cover bushings.

Tube Cubicles

Four tube groups, G_1 through G_4 are shown in Figure 1. Each tube group is connected to a three-phase transformer secondary winding, two to the 60-cycle transformer and two to the 25-cycle transformer, respectively. The tube groups are connected in series by the d-c link which includes the d-c reactor. The alternate arrangement of the 25- and 60-cycle tube groups in the d-c loop minimizes the voltage level because the 60- and 25-cycle halves function oppositely at all times—one serving as rectifier to give direct voltage, the other serving as inverter to absorb the direct voltage. The insulation stress of the transformer secondary windings and d-c link is further reduced by grounding the neutral of one of the transformer secondary windings. By this arrangement the insulation stress is only half the direct voltage of one tube group, although the effective operating voltage is twice the direct voltage of one tube group. There are six tubes in each tube group (24 tubes for one 10,000-kw unit) arranged in a three-phase double-way circuit as shown in Figure 3.

The two 60-cycle tube groups are enclosed in one cubicle, and the two 25-cycle tube groups are in another. Two cubicles are required for each 10,000-kw unit. Each enclosure completely surrounds 12 tubes, with a door and window in front of each tube for easy access and visibility. Figure 4 shows an interior view of one tube enclosure taken through an open door. The tube cubicles are about 10 feet wide, 20 feet long, and 14 feet high. Tubes which are 180 degrees apart in phase position are mounted back to back with the cathode of one tube connected to the anode of the other. The lines from the secondary winding of the transformer come from one side and drop down to make connection. The tubes, together with a box for ignitor and grid-control apparatus, are mounted on a platform insulated from ground. Water for cooling the tubes is brought to the tube through a porcelain insulating coil, which gives a length of water column sufficient to withstand the voltage appearing from tube to ground. On top of the grid box is mounted the potential-dividing-resistor rack which keeps the intermediate anode at a potential halfway between anode and cathode. On the back of the grid box is mounted the filter capacitor which connects to the filter resistor located in the top of the cubicle. There is one filter capacitor and resistance for each tube. Insulating transformers standing in the center of the cubicle provide means for bringing the ignitor pulses and grid-control voltages up to the high-potential level of the tubes and grid box.

R–C Filters

The resistance–capacitance filter connected across each tube serves to damp the oscillations caused by commutation. When a tube fires, the positive voltage across the tube suddenly drops to zero. Likewise, when a tube stops conducting, a high negative voltage rapidly is impressed on the tube. These sudden voltage changes cause oscillation in a circuit containing inductance, stray capacitance, and very little resistance. The oscillations produced by the voltage shocks increase the stress on the tubes. This added voltage is especially serious in the interval immediately following commutation. However, if additional capacitance be added across the source of the shock voltages with sufficient resistance in series to damp the system rapidly, the magnitude and duration of the objectionable oscillations will be reduced. By damping the oscillations the radio and telephone interference is also reduced. The resistance–capacitance filter across the tubes also will serve to absorb the inductive discharge which sometimes is associated with an arc-back. In this way the filters will prevent the

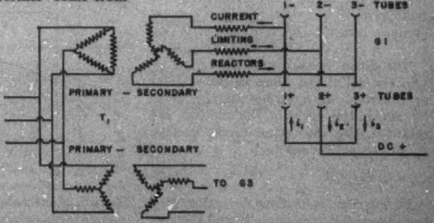


Figure 3. Circuit diagram for six tube groups arranged in three-phase double-way circuit

Arrows show path of arc-back currents

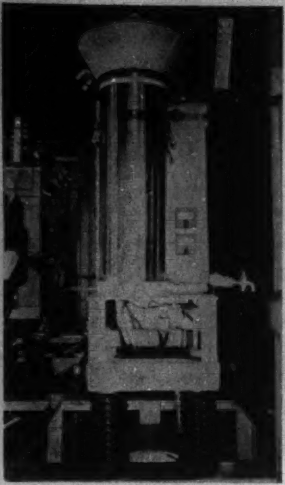


Figure 4. Two 10,000-kw electronic power converters transfer power between the Carnegie-Illinois Steel Corporation's 25-cycle 44-kv and their 60-cycle 69-kv systems

Twelve-tube electronic converter enclosure. Interior view. Center: type *GL*-506, pentode ignitron rectifier–inverter tube mounted front of grid-control equipment on insulated platform. Additional grid control at left rear. Excitation insulating transformer below

high-voltage surges sometimes encountered in rectifier operation.

Three-Phase Double-Way Circuit

The three-phase double-way circuit shown in Figure 3 was chosen because of its high apparatus economy and good operating characteristics. The no-load direct voltage E_0 (neglecting arc drop) for the three-phase double-way circuit is

$$E_0=\frac{3\sqrt{2}E_L}{\pi}=1.35E_L \tag{1}$$

Here E_L represents the effective value of the alternating line-to-line voltage supplied to the tubes. An alternating line voltage of 12,970 volts has been chosen, giving a direct no-load voltage of 17,500 volts for both the 25- and 60-cycle sides.

The direct voltage may be varied by grid control of the tubes in the rectifier or inverter. If α represents the angle by which the grids of the rectifier are retarded, the theoretical direct voltage E'_{d0} of the rectifier will be

$$E'_{d0}=E_0\cos\alpha \tag{2A}$$

In inverter operation the grids may be advanced by the angle β, and the corresponding theoretical counter electromotive force E''_{d0} will be

$$E''_{d0}=E_0\cos\beta \tag{2B}$$

The current-limiting reactance together with the leakage reactance of the transformers causes a reduction of the direct voltage when operating as a rectifier and an increase in the direct countervoltage when operating as an inverter. Curves showing the voltage characteristics are given in the Cramer–Morton–Darling paper. The direct voltage change, E_x, drop for a rectifier or rise for an inverter is given for the three-phase double-way circuit by the relation

$$E_x={}^1/_2X_{1/1}I_{1/1}E_0 \tag{3}$$

Here E_0 represents the no-load direct voltage, $X_{1/1}$ represents the per unit reactance, and $I_{1/1}$ represents the per unit load current. (Relation 3 does not hold when the voltage change due to reactance exceeds one quarter of the no-load voltage.)

If the theoretical direct voltage is corrected for tube arc drop E_a, reactance drop E_x, and for transformer copper losses, the output direct voltage of the rectifier E'_{dc} will be

$$E'_{dc}=E_0\cos\alpha-2E_a-I_{1/1}(R_{1/1}+{}^1/_2X_{1/1})E_0 \tag{4}$$

Here $R_{1/1}$ represents the per unit transformer resistance.

A similar relation for the inverter voltage E''_{dc} is obtained by adding the arc voltage and the resistance and reactance voltages to the theoretical direct voltage E''_{d0}.

$$E''_{dc}=E_0\cos\beta+2E_a+I_{1/1}(R_{1/1}+{}^1/_2X_{1/1})E_0 \tag{5}$$

The 25- and 60-cycle transformers will be assumed to have equal copper losses, and the current-limiting reactances also should be equal for reversible operation. The load current and direct voltage must be the same for the rectifier and inverter. These simplifying conditions give

$$I_{1/1}=\frac{\cos\alpha-\cos\beta-4\dfrac{E_a}{E_0}}{X_{1/1}+2R_{1/1}} \tag{6}$$

The arc-drop loss in the tubes $4E_a/E_0$ will be less than one per cent of the load at any load. Equation 6 shows that the angle of inverter advance β must be greater than the angle of rectifier retard α. Increasing β or decreasing α will raise the load. In practice, it is desirable to control α to regulate the load flow to the desired value. In the case of a low voltage on the rectifier side or high voltage on the inverter side, it may be impossible to obtain the desired load by reducing α to zero. It then will be necessary to transfer the function of load control to the inverter and increase β to obtain the desired load. For normal-voltage levels and loads, the load will be controlled by the rectifier grids for both directions of power flow. The inverter grids will be adjusted to provide an ample deionization time. The deionization time required will be discussed next.

Inverter Deionization Time

One of the important differences between grid-controlled gas tubes (such as the mercury-vapor tube) and a high-vacuum-type tube is the fact that a negative grid voltage does not reduce the current flowing in the gas tube. The grid however can prevent the current from starting. The failure of a negative grid in interrupting current arises from the fact that the negative grid will be neutralized by a positive-ion sheath if the tube is carrying current. If the tube

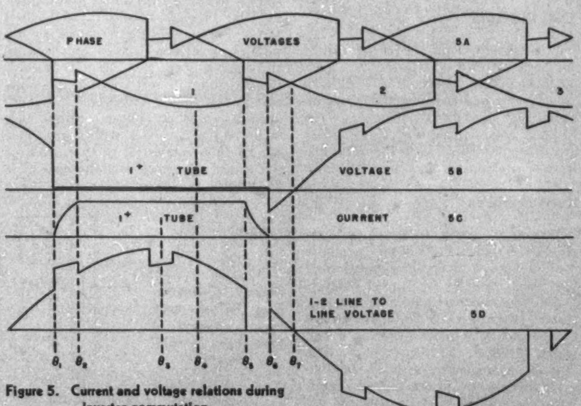


Figure 5. Current and voltage relations during inverter commutation

is not already carrying current, there are no positive ions to form a sheath, and the negative grid can function to prevent conduction. The inability of gas tubes to cut off current should not be considered a limitation but rather an advantage, because the current through a tube decreases normally to zero and then ceases, whereas, if the tube were able to cut the current off, it would be likely to induce high voltages in inductances included in the circuit.

When a gas tube is conducting it contains a plasma of positive ions. This plasma of positive ions requires a small time to disappear or deionize. It is therefore evident that the grid of a gas tube is not able to gain control (prevent current conduction) until a short interval has elapsed after conduction. This interval required for regaining control is called the deionization time. This should not be interpreted as meaning that no positive ions remain in the tube after the deionization time, but rather that their density is such that they do not interfere with the function of a negative grid. (The deionization time probably could be described more accurately as control time.) The deionization time of the tubes is discussed in the paper by Steiner, Zehner, and Zuvers.[3]

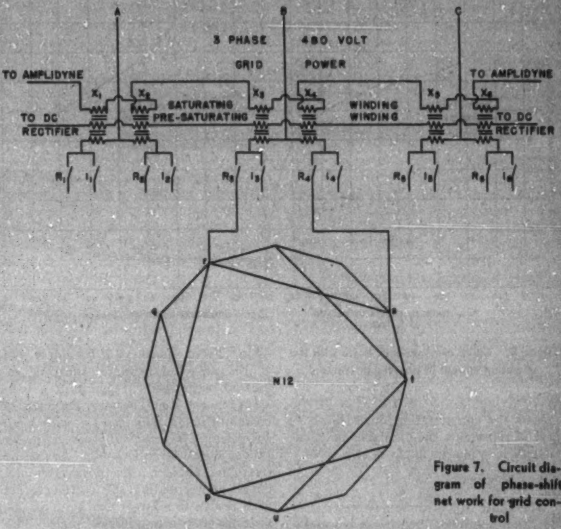


Figure 7. Circuit diagram of phase-shift net work for grid control

Inverter Commutation

During the deionization time the anode of the tube must be held negative to prevent conduction. The duration of the negative-anode voltage may exceed the deionization time required by the tube, but the deionization time sets a minimum duration for the negative-anode voltage. Figure 5 shows how the negative-anode voltage, following conduction, is obtained. The line-to-neutral voltages applied to the tube group are shown in 5*A*. The anode-to-cathode voltage and the current of a tube are shown in 5*B* and 5*C*, respectively. The voltage between the lines to which tubes 1+ and 2+ are connected is shown in 5*D*. Inverter operation is indicated by the fact that the line-to-neutral voltage for tube 1+ is negative during the interval when this tube is carrying current. Commutation from tube 1+ to tube 2+ begins at the instant θ_5 in Figure 5 when tube 2+ is fired by its grid. This tube must be fired before its line-to-neutral voltage equals that of tube 1+ which occurs at θ_7. The angle by which the grid is fired ahead of θ_7 is called the angle of grid advance. When tube 2+ is fired ahead of θ_7 the current transfers from tube 1+ to tube 2+ because the counter electromotive force in the phase of tube 2+ is lower than that of tube 1+. The voltage difference of tubes 1+ and 2+ is the commutating voltage. The action is quite similar to commutation in a d-c motor when the brushes are shifted against the direction of rotation. In Figure 5 the grids are assumed to be advanced 30 degrees (θ_5 to θ_7).

During commutation from tube 1+ to tube 2+ the a-c lines to those tubes are short-circuited, and the commutating voltage (difference between phase voltages) is absorbed in the line reactors. This effect is shown by the notches in the voltage waves. The angle of commutation, θ_5 to θ_6, is here assumed to be 15 degrees. If the commutating voltage persists after the current has been transferred to tube 2+, the remaining commutating voltage will be in a direction to reverse the current through tube 1+ and will appear as a negative voltage across the tube (see 5*B* between θ_6 and θ_7). The negative-anode voltage which disappears at θ_7 can be seen clearly in the line-to-line voltage of 5*D* between θ_6 and θ_7. During this interval the tube must deionize, because at θ_7 positive voltage will be impressed, and a loss of control will result if the tube has not regained control.

Inverter Operation

It is evident that the angle of grid advance θ_5 to θ_7 which was represented by β in equation 2*B* equals the commutating angle θ_5 to θ_6 plus the available deionization time θ_6 to θ_7. Writing this in the form of an equation where μ rep-

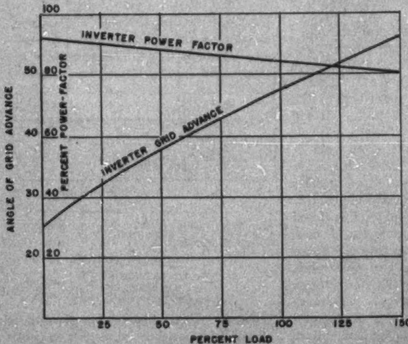


Figure 6. Inverter power factor and grid advance angle for constant deionization time

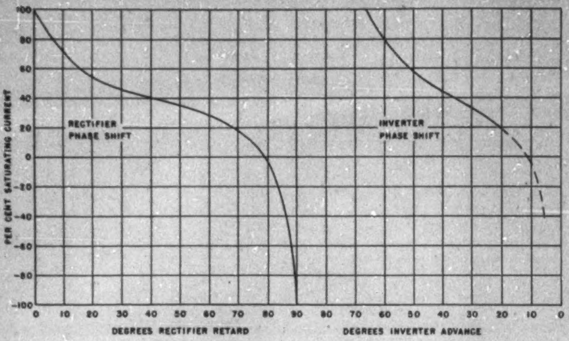


Figure 8. Curve of phase-shift angle with d-c saturating current in phase-shift reactors

resents the commutating angle θ_5 to θ_6 and M represents the available deionization angle or margin angle, θ_6 to θ_7 gives

$$\beta=\mu+M \tag{7}$$

The angle M (θ_6 to θ_7) has been called the "margin angle," because it is usually larger than the minimum required for deionization and provides a safety factor in commutation. If the voltage drops or the load increases without a corresponding increase in the angle of advance, the margin angle will be absorbed partly by the greater commutating angle without resulting in insufficient commutation or deionization time.

The equivalent power-factor angle of the inverter depends on the angle between the center of the current wave θ_3 and the center of the voltage wave θ_4 (see Figure 5). The center of moment of the current wave should be used for accuracy. If the rate of current transfer during commutation be assumed constant, the power-factor angle θ is approximately

$$\theta={}^1/_2\mu+M \tag{8}$$

The angle θ represents the phase angle between the fundamental component of the current and the voltage. The effect of the current harmonics is neglected in equation 8. The power factor as determined by effective current and voltage readings which includes harmonics will be approximately

$$\text{Power factor}=0.955\cos({}^1/_2\mu+M) \tag{9}$$

The factor 0.955 assumes a rectangular wave shape of current. For precise calculations this should be corrected by the overlap factor.[8]

Equation 9 shows that a large margin angle will result in a low power factor of operation. It therefore is desirable to keep the margin angle as small as possible. From equation 7 it is evident that β, the angle of advance, must be increased with load to maintain a constant margin angle, because the angle of commutation μ will increase with load. Increasing β however will cause a greater load as shown by equation 6. As a result it is found that increasing the inverter load angle, while holding the rectifier grids fixed results in a larger load with an approximately constant margin angle. The curves in Figure 6 show a typical relation between inverter grid advance and load for a constant margin angle. The corresponding inverter power factor is also shown in Figure 6.

Phase-Shift Circuit

The phase-shift circuit now may be considered in the light of the previous discussion. The inverter grid angle must be varied with load to maintain a safe margin angle. The rectifier grids should be controlled to maintain the desired load. When changing from rectifier to inverter operation for reverse power flow the phase position of the grid voltage must be shifted by approximately 150 degrees. These functions are performed by the phase-shift network shown in Figure 7.

Power for the grid and ignitor circuits is furnished by an auxiliary power transformer connected to the high-voltage lines as indicated in Figure 1. Details of the switchgear are described in the paper by Gittings and Bateman. It will be assumed here that three-phase 480-volt grid power will be delivered to lines A, B, and C of Figure 7. These a-c lines are connected through d-c saturated reactors X_1 to X_6 inclusive to two six-pole contactors R and I. The R contactor is closed for rectifier operation and the I contactor for inverter operation. These contactors introduce the grid phase shift necessary for reversing the power flow. Suitable interlocks prevent simultaneous closing of the R and I contactors. These contactors connect the three-phase a-c lines to a 12-phase network $N12$ also shown in Figure 7.

Assume that the R contactor is closed. Each a-c line then will be connected through the d-c saturated reactors to two points on the 12-phase network. Thus line B is connected through reactor X_3 to point r on the 12-phase network, and also line B is connected through reactor X_4 to point s. Points r and s are separated by an angle of 90 degrees on the network. In a similar manner line A is connected to points p and q through reactors 1 and 2, respectively, and line C is connected through reactors 5 and 6 to points t and u. If the odd-numbered reactors are fully saturated and the even-numbered reactors unsaturated, lines A, B, and C are closely connected to points p, r, and t, respectively. Reversing the saturation of the reactors, so that the odd-numbered reactors are unsaturated and the even-numbered reactors are saturated, shifts the lines A, B, and C to points q, s, and u, respectively. It is therefore evident that the 12-phase network can be given a 90-degree phase shift by reversing the saturation of the d-c reactors. Fortunately this shift is fairly continuous with saturating current as is shown in Figure 8 which gives a curve of the angle of phase shift versus direct saturating current. The voltage variation of the 12-phase network need not exceed ten per cent over a phase-shift range of 90 degrees. The time required for a complete phase shift of 90 degrees need not exceed 0.1 second for a 60-cycle network. Each 10,000-kw unit has one 60-cycle and one 25-cycle phase-shift unit. All tube-control power, grids, ignitors, holding anodes, and so on, for two-tube groups is furnished by one 12-phase network. When the R contactor is closed, the saturating reactors span an angle of 90 degrees, and the grids may be advanced continuously

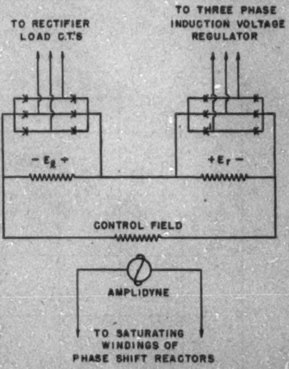

TO RECTIFIER LOAD C.T'S
TO THREE PHASE INDUCTION VOLTAGE REGULATOR
− Eg +
+Er −
CONTROL FIELD
AMPLIDYNE
TO SATURATING WINDINGS OF PHASE SHIFT REACTORS

Figure 9. Circuit diagram for Amplidyne control-field excitation for rectifier operation

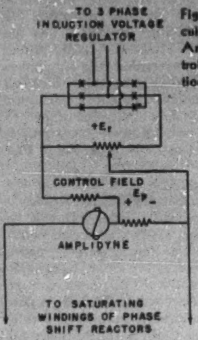


Figure 10. Circuit diagram for Amplidyne control-field excitation for inverter operation

through an angle of 90 degrees, beginning at cutoff or 90 degrees grid retard for rectifier operation. When the *I* contactor is closed, the saturating reactors span an angle of only about 60 degrees, and the grids may be advanced continuously through an angle of 50 degrees beginning at about 20 degrees advance for inverter operation. Taps on the 12-phase network give easy adjustment of the range and limits of phase shift for both rectifier and inverter operation.

Each d-c saturated reactor has two saturating windings as shown in Figure 7. One winding may be called the presaturating winding, and the other the saturating winding. The six presaturating windings are all connected in series and supplied with direct current from a constant-potential rectifier of the dry-plate type. The saturating windings also are connected in series, but the polarity of the windings on the odd-numbered reactors is opposite to that of the even-numbered reactors. A direct current through the saturating windings affects the odd and even reactors oppositely, increasing the saturation of one by aiding the presaturating winding, and decreasing the saturation of the other by opposing the presaturating winding. It is thus possible to shift the grids through the available range by reversing the current in the saturating windings. For each value of saturating current there is a corresponding phase position of the 12-phase network. Shifting the 12-phase network by d-c saturation shifts the operating phase of two six-tube groups without disturbing their 12-phase relation, nor does it disturb the timing of ignitors, grids, and holding anodes for any tube. The current for the saturating winding is supplied by an Amplidyne. The control field of the Amplidyne is varied to give the desired grid shift.

Phase-Shift Amplidynes

The Amplidyne armature is connected permanently to the saturating winding of the reactors. The excitation for the Amplidyne control field is changed simultaneously with the *R* and *I* contactors when shifting from rectifier to inverter operation or the reverse. In rectifier operation the grids normally are retarded 80 degrees, and the Amplidyne current serves to advance the grid phase angle, until the desired load is obtained. Here the Amplidyne serves as a current regulator balancing the load current against a reference signal. A measure of the load current is secured by rectifying the secondary current of the a-c line current transformers to give a direct current proportional to the alternating load current of the rectifier. Passing this rectified current-transformer current through a resistance gives a direct voltage proportional to the alternating load current, which may be called the load voltage E_1. This load voltage will oppose a second direct voltage E_r which may be called the reference voltage. The difference between the reference voltage and the load voltage will act on the control field of the Amplidyne as shown in Figure 9. If the load is lower than desired, the reference voltage E_r will exceed the load voltage E_1 and will excite the Amplidyne in a direction to advance the rectifier grids. If the load be greater than required, the reverse action will take place, and the rectifier grids will be retarded to reduce the load current.

The reference voltage E_r is obtained by rectifying the output of a three-phase induction voltage regulator. This induction regulator is motor-driven in response to the several methods of regulating the load, that is, manual, watt regulator, and so on, as described in the Cramer–Morton–Darling paper. The motor drive for the load-regulating induction device is described in the paper by Gittings and Bateman.

Upon changing to inverter operation, the excitation signal for the phase-shift Amplidyne is changed to a circuit as shown in Figure 10. Here a voltage E_p proportional to the Amplidyne current is balanced against the reference voltage. When the reference voltage calls for a larger load, it also will act through the Amplidyne and phase-shift circuit to advance the inverter grids. In this way, the inverter grids are advanced to maintain an approximately constant-margin angle. The Amplidyne control circuit as shown in Figures 9 and 10 represents only the essential operating functions of the Amplidyne control. Additional features provide correction for voltage-level differences between the two systems, phase unbalance in the inverter, and transfer control when the rectifier is unable to attain the desired load. These complications have been omitted for simplicity.

Ignitor Circuit

In the pentode ignitron tubes used in this installation, the cathode spot must be started each cycle by means of an ignitor. A magnetic-firing circuit of the type described in a paper on sealed-tube ignitron rectifiers by M. M. Morack and H. C. Steiner[4] is used to fire the ignitors. Figure 11 shows the circuit diagram of the firing circuit. The usual provision for phase-shifting the ignitor circuit may be omitted here, as the entire 12-phase network supplying the ignitor power is shifted through the desired angle. A Nicaloi core reactor *VLL* is shunted across the circuit immediately following the first linear reactor. This reactor is designed to operate somewhat above saturation and reduces the voltage applied to the rest of the circuit because of the reactive current drawn. Since the Nicaloi saturates sharply, it will tend to hold a constant voltage. This means the input voltage may be varied over wide limits without affecting the successful operation of the firing circuit. The part of the circuit from the input to the ignitor insulating transformer *IIT* is mounted at ground potential. The insulating transformer is used to transfer the ignitor firing peaks up to the high-potential level of the tubes. The ignitors of two tubes which are 180 degrees apart in phase position are fired by one firing circuit. Since this firing circuit furnishes current for only 18 degrees after the ignitor fires, a holding anode is used to maintain the cathode spot after the ignitor starts it. This holding anode is shown in the grid circuit of Figure 12, as HA_1.

Grid Circuit

The grid circuit performs two principal functions: it provides a negative bias for the grid at all times except when the tube is to be conducting, and it furnishes a sharp positive firing voltage at the time that the tube should start conduction. In addition to these two functions, the grid circuit furnishes power for the holding anode and for the heater for the main anode. The grid-insulating transformer *GIT* in Figure 12 isolates the grid circuit from ground potential, in the

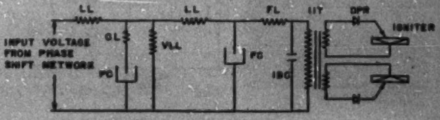


Figure 11. Magnetic ignitor firing circuit

same manner that *IIT* isolates the igniter circuit in Figure 11. Two tubes which are 180 degrees apart in phase position are served by *GIT*. The input for the grid-insulating transformer comes from the 12-phase network at a suitable phase position. The anode heater, *AH* of Figure 12 and the holding-anode transformer *HAT* of Figure 12 are connected directly to the grid-insulating transformer. Negative bias for the grid is supplied by a biphase double-way rectifier *BR* connected to the grid-bias transformer *GBT*. A large capacitor *C* and a Thyrite resistor *TR* tend to filter and regulate the grid bias.

For a phase-commutated inverter, accurate firing is essential, since a small error in the firing point will mean a much greater change in the margin angle. Peaking transformers which give a steep rise of voltage are used for firing the grids to insure accuracy.[5] The positive voltage impressed on the grids must exceed the negative grid-bias voltage before the grid will be fired. The peaking-transformer voltage rises so rapidly that there is a negligible error in grid firing even if one tube requires a greater positive grid voltage than another.

An undesirable situation arises if the ignitor fails to fire and a cathode spot is not produced. It has been observed that, when the grid is made positive with no cathode spot available, the residual ionization may form a cathode spot by bombardment. This condition is discussed in the paper by Steiner, Zehner and Zuvers.

In the grid circuit of Figure 12 the firing impulse from the peaking transformer is introduced in the grid circuit through the grid-coupling transformer. The holding anode HA_1 is included in the circuit connecting the peaking transformer and the grid-coupling transformer. In this way the positive firing impulse of the peaking transformer is impressed on the grid only if there is a cathode spot in the tube to complete the circuit through the holding anode HA_2. Failure to form a cathode spot in the tube prevents the positive firing voltage from being impressed on the grid. This interlocking of the grid action with a cathode spot makes it possible to block the operation of the tube by removing the ignitor firing impulse. This is discussed under ignitor blocking to remove fault currents in the rectifier.

At every instant the direct current passes through four rectifier and four inverter tubes in series. It is therefore obvious that all eight tubes must be ready to start conduction, that is, have a positive grid voltage, at the same time. Since the tubes in one rectifier or one inverter fire 60 degrees apart, and the two rectifiers or inverters are displaced 30 degrees in phase position, some grids must remain energized for at least 90 degrees. For purposes of symmetry, all grids remain positive about 100 degrees. The duration of the positive-voltage wave is determined by the peaker design. One peaking transformer supplies the firing impulse for both the control and shield grids. A resistor is placed in series with the shield grid to make sure that the control grid also will fire after the shield grid is conducting. Any current into the shield grid will cause a potential between the two grids in a direction to draw ionization up to the control grid. The ignitor is fired ahead of the grid so that, even with a hard firing ignitor, the grid will become positive after the ignitor has fired and the grid will initiate conduction in the tube. A voltmeter to measure the bias voltage and an ammeter to measure the holding-anode current are provided in each grid circuit to facilitate checking the operation.

Rectifier Faults—Arc-Back

An arc-back is the failure of a rectifier tube to hold off negative anode-to-cathode voltage during the inverse part of the cycle. When a tube arcs back, a cathode spot is formed on the anode with the result that the rectifier tube will carry current in the reverse direction. It loses its rectifying properties for the duration of the arc-back. In the three-phase double-way circuit, the only tubes that furnish current to the tube which has arced back are the two tubes connected to the same d-c bus and to the other two phases of the transformer. The arc-back currents are shown by the arrows in Figure 3.

Suppose tube 1+ arcs back near the start of the time during which it is holding off inverse voltage. Tube 2+ will be conducting, since the transformer voltage on phase 2 is most positive. Since tube 1+ had negative anode-to-cathode voltage before it arced back, the voltage of the transformer will make current flow in the reverse direction on through tube 1+, into phase 1 of the transformer, out of phase 2 and through tube 2+ in the forward direction. The arc-back current will be limited largely by the transformer and a-c line reactance. When phase 3 becomes more positive than phase 2, and tube 3+ fires, the current will transfer from tube 2+ to tube 3+, still flowing in the reverse direction through tube 1+. Tube 3+ will carry the current until tube 2+ again fires or until the arc-back current falls to zero. During the time when phase 1 is most positive, and tube 1+ normally would be conducting in the forward direction, the transformer voltage on phases 1 and 3 is in the direction to reduce the arc-back current. When the rectifier is running with a little phase retard, the arc-back current generally goes out before tube 2+ fires again. Tube 1+ regains its ability to stand inverse voltage, and load again is carried with only a one-cycle interruption. The greater severity of arc-backs in installations where there are several rectifiers in parallel is due to the fact that the parallel rectifiers also furnish current to the tube which has arced back. This gives a much higher arc-back current, which will not go to zero until the circuit is opened, generally by anode breakers.

Ignitor Blocking

Occasionally, the tube which arced back cannot hold off negative voltage on the next cycle, because of high gas pressure around the anode, or a hot spot on

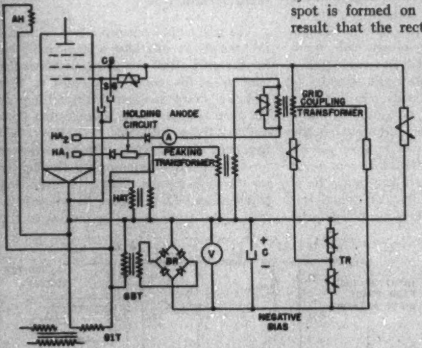
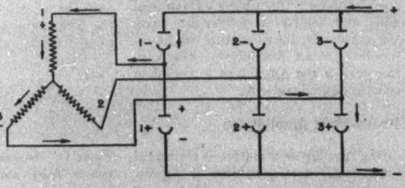


Figure 12 (left). Grid and holding-anode circuit

Figure 13. Three-phase double-way inverter circuit showing normal current flow



the anode, and it will continue to arc-back each cycle. Recovery from this multiple arc-back condition is obtained by blocking the ignitors on all tubes, thereby preventing the two other tubes on the same rectifier from furnishing any current to the tube which arced back. The ignitors are blocked for a time long enough to enable the tube which arced back to regain control. The ignitors are blocked by the ignitor blocking contactor *IBC* shown in Figure 11. When this contactor closes, it short-circuits the ignitor firing impulses to the entire tube group. The ignitor blocking contactor is operated by an instantaneous overcurrent relay in the a-c-lines. The arc-back current flowing in these lines initiates the ignitor blocking sequence. When the alternating line current has fallen to the relay dropout value the ignitor impulses are restored. The arc-back current is interrupted in about 0.05 second, and normal power operation may be resumed in about 0.2 second.

Inverter Faults

The inverter furnishes a counter electromotive force, which is in the wrong direction to produce current through the d-c circuit. Current must be forced against the counter electromotive force of the inverter by a slightly higher rectifier voltage. Figure 13 shows current paths when current is being carried by tubes 3+ and 1−. Current flows through tube 1−, through phases 1 and 3 of the transformer against the instantaneous voltage of the transformer, and through tube 3+. The tubes are fired in the sequence 1+, 3−, 2+, 1−, 3+, 2−, so as always to present a transformer voltage opposing the rectifier direct voltage and opposing the flow of current. Power is absorbed by the inverter transformer and fed into the receiving system when current is forced through the transformer windings in opposition to their instantaneous voltage.

While tube 1− is conducting, tube 1+ has a positive-anode-to-cathode voltage equal to the direct voltage minus the arc drop of tube 1−. During the major part of the nonconducting portion of the cycle, an inverter tube must hold off positive voltage. If, while tube 1− is conducting, tube 1+ fails to hold off positive voltage—that is, becomes conducting in the forward direction—then tubes 1+ and 1− both are conducting; this allows the rectifier current to pass through two tubes without opposition from the transformer counter electromotive force. This condition frequently is called a "loss of control" and constitutes a short-circuit on the d-c lines. This is the characteristic inverter fault. Whatever initiates an inverter fault, tube failure, misfire, or insufficient deionization time, the immediate result is a short circuit on the d-c lines. This fault fortunately is not reflected into the inverter power system because of the phase position of the inverter grids. The inverter will continue to try to commutate the direct short-circuit current, but this will not result in abnormally high currents in the inverter transformer. When the direct current drops to a normal load value, the inverter will be able to commutate this current and will resume operation.

It should be evident from this discussion that the only condition required to resume operation after an inverter loss of control is to reduce the direct short-circuit current. This may be accomplished, as in the case of an arc-back, by blocking the rectifier ignitors. Either arc-back or inverter loss of control will be cleared successfully by the ignitor blocking, and operation will be resumed if there are no persistent tube faults.

The oscillograms shown in Figure 14 give the currents taken during an inverter loss of control caused by short-circuiting an inverter ignitor to make a misfire. The alternating currents shown are those in the secondary winding of the main transformers. Each trace shows the currents in the plus and minus tubes on that phase. The arrow points to the location of the misfire on the 1+ tube, which caused tube 3+ to continue to carry current until tube 3− fired. At this time tubes 3+ and 3− were conducting simultaneously and caused a short circuit on the rectifier. The 60-cycle rectifier overcurrent was reduced to zero in two cycles as shown in the oscillogram. Meanwhile tubes 1+ and 1− were trying to commutate the direct current away from 3+ and 3− but without success. After the fault was cleared, there was a delay of several cycles before the frequency changer resumed operation under load.

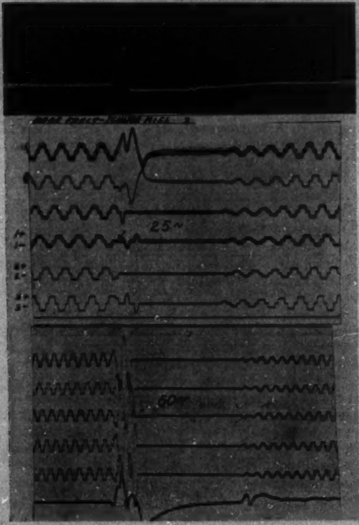


Figure 14 (left). Direct and alternating currents during inverter shoot-through produced by causing an ignitor misfire in the inverter

Figure 15 (right). Test panel for converter

Indicating Selsyn for phase position of excitation at top. Cathode-ray oscilloscope and selective switches for tube current and voltage in center. Fault-indicating counter at bottom

Transient Indicator

In an installation containing 48 tubes it is desirable to have testing equipment to determine which tube or tube circuit is involved if rectifier arc-back or inverter loss of control occurs. When a fault occurs, the first disturbance in the tube voltages will appear across the group of tubes that initiated the fault. By proper connection of a circuit to pick up this voltage disturbance, the origin of the fault is located in a group of three tubes. Another part of the indicator locates the approximate phase positon in the cycle at which the fault occurred. Still another part of the indicator, which utilizes the transformer currents, indicates whether or not the fault was an arc-back. Counters are used to record the information obtained by the transient indicator which is automatically reset.

Grid-Phase-Position Selsyns

During operation, it is desirable to be able to tell quickly and easily the firing point of the rectifier and inverter tubes with respect to their anode voltages. This gives a quick check that the excitation controls are functioning correctly. This information is provided by means of indicating Selsyns, connected to the phase-shift network which is shifted in phase position to vary the firing point of the tubes. This voltage of varying phase position from the network is connected to the rotor of the Selsyn. A voltage fixed in phase position is connected to the stator. Thus, as the network voltage assumes a given phase position, the rotor will assume a given angular position with respect to the stator. As the rotor voltage varies in phase position, the rotor will vary correspondingly in angular position, and a direct indication of the firing point of the tubes will be given. The Selsyn scale has a range of 180 degrees, covering both rectifier and inverter operation. The rectifier section covers 90 degrees, the range from full to zero direct voltage, and is marked in degrees retard. The inverter range covers 90 degrees and is marked in degrees advance. These indicating Selsyns are shown at the top of Figure 15.

Equipment for Observing Tube Voltages and Currents

Because of the new features of this installation, it was felt that means should be available for observing the tube voltages and currents during operation. Current and potential transformers are connected to the main-transformer secondary lines to furnish the desired information. The voltage drop across a resistor in the current-transformer secondary circuit is used to represent the current. A cathode-ray oscillograph is mounted in the main switchboard for observing all currents and voltages. The cathode-ray oscillograph and selective switches are shown in the center of Figure 15. Selective switches permit convenient connection to any current or voltage. The typical line-to-line secondary voltage for inverter operation is shown in 5*D*. The points where the sine wave crosses the axis are points where two phase voltages are equal. For inverter operation, these represent the fully retarded points. Each zero point corresponds to one of the two tubes which operate 180 degrees apart. For rectifier operation, the maximum voltage or fully advanced point is 180 degrees advanced from the fully retarded inverter point for that same tube.

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A Method of Approach to the Consideration of Power-Distribution-System Costs

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THE design of the various component parts of a power-distribution system to obtain low cost has been discussed at length in many published articles. These have, in general, considered either specific parts of the system only, or all parts of the system but for only one specific set of conditions, such as load density, system voltages, continuity of service, and so forth. It is the purpose of this paper to present a generalized method of approach to the consideration of power-distribution-system costs for various combinations of conditions when considering collectively the costs of each of the three basic elements which make up the so-called distribution system. These elements are:

1. The subtransmission circuits supplying the distribution substations.

2. The substations which transform from the subtransmission to the primary voltage.

3. The primary distribution system.

It has been found that consideration of the cost of any one of these parts without reference to the others may lead to erroneous conclusions as to the best over-all system from a cost standpoint. That is, the use of the minimum-cost design for one part may require expenditures for the other parts of the system which result in an over-all system cost higher than necessary if all parts are reviewed jointly and designs evaluated on an over-all basis. It is believed that this kind of approach, wherein all elements of the system are included, is one which will give a complete picture of system costs.

Scope and Method of Approach

In making any specific system analysis of this type all costs should be included as they apply to the particular system under consideration. These should include cost of equipment and lines, installation based on local labor and material costs, real estate, losses, maintenance, and operation. In order to present this method of approach, however, a rather generalized system was analyzed on the basis of:

I. Actual investment involved in the original system cost.

II. Evaluated losses in each system.

III. Possible deferment of system investment by using small substation sizes.

Hypothetical systems utilizing modern design principles were assumed and although rather liberally generalized were laid out in detail from the subtransmission to the secondary distribution circuits. Estimated installed cost figures were used for all equipment and lines, and losses

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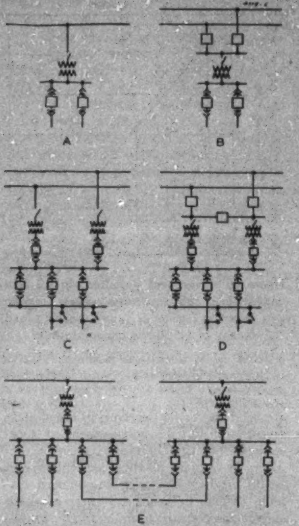
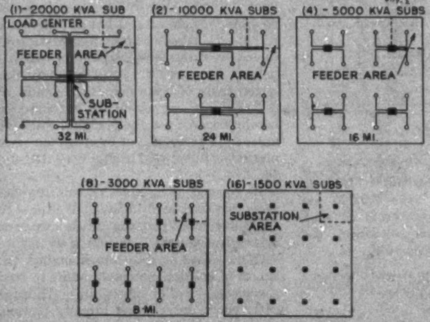


Figure 1. Typical one-line diagrams illustrating five basic systems analyzed and showing connections to subtransmission and distribution circuits

- A. Simple radial
- B. High-voltage selective
- C. Low-voltage selective
- D. High-voltage–low-voltage selective
- E. Primary network

Figure 2. Substation locations and 4.16-kv feeder layout

All circuits are 4/0 conductor except 500,000 circular mils where cable is used for primary network. Construction is three-phase, four-wire for overhead with three feet equivalent delta spacing; for underground, three-conductor cable with separate neutral conductor. Mileage figures are total for circuits shown. Area 16 square miles



were evaluated on the basis of a kilowatt-hour charge without a demand charge. All system investment and loss evaluations were reduced for comparison purposes to terms of dollars per kilovolt-ampere of system load.

There are a number of factors which must be considered when studying an electric-utility distribution system to determine the most economical type of system to use to serve a given load area. The more important of these factors are:

1. Voltage conditions required within the area.
2. Degree of service continuity required.
3. Size of load area and load density (present as well as future).
4. Subtransmission voltages available.
5. Whether subtransmission and/or primary circuits are overhead or underground
6. Size of substation to be used.

All of these factors and the relative effects of each on the over-all economics of distribution-system design were analyzed. Secondary networks or primary distribution voltages above 4.16 kv were not considered.

Conclusions

The method used shows that investment in subtransmission circuits, substations and distribution circuits, and deferment of system investment must all be considered. Quite a wide change in many of the variable quantities will not change the general conclusion that small substations, 5,000 kva and below, are more economical in most cases than the large substations from 10,000 to 20,000 kva. However, the number of possible variations is such as to require a thorough study of a specific system in order to obtain the correct answer as to the most economical system or size of substation to be used.

A number of specific assumptions were made and this should be kept clearly in mind in considering the following rather general conclusions. It is true that the results of an analysis of this kind are indicative only and that the correct answer for a specific system can be determined only after a study of that system; nevertheless, it is believed that this type of generalized study will allow the establishment of bench marks which can be quite useful.

Certain definite substation sizes involving standard transformer ratings were used. Results are plotted in terms of these substation sizes and correct values for other sizes cannot be obtained by interpolation of the values shown.

1. The simple radial system (Figure 1A) using substations rated 1,500 to 3,000 kva is the most economical.

2. Where a degree of reliability better than that obtainable with the simple radial system (Figure 1A) is required, the primary network (Figure 1E) is the most economical system in most cases.

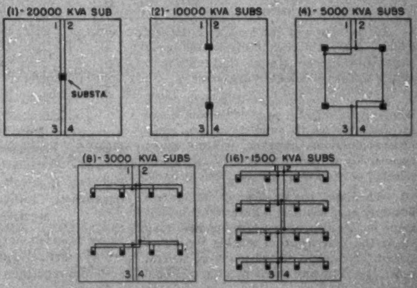
3. For the primary network, substations rated 3,000 kva to 5,000 kva will be the most economical, the smaller sizes in general being more applicable to overhead subtransmission and the larger sizes to underground subtransmission.

4. For any particular system type other than the simple radial or primary network, the most economical unit is 3,000 to 5,000 kva where subtransmission is overhead and 5,000 to 20,000 kva where subtransmission is underground. Lighter load densities (about 1,500 kva per square mile or less) and systems with lesser degrees of duplica-

Figure 3. Substation locations and subtransmission layout for four 13.8-kv circuits

Duplicate circuits to each substation. Typical for systems B, C, and D of Figure 1

Total Load on Each Substation	Wire Size		Circuit Miles 1,500 Kva Per Square Mile
	Overhead	Underground	
20,000 kva	4/0	400	8
10,000 kva	4/0	400	4
10,000 kva	2/0	300	2
5,000 kva	4/0	400	6
5,000 kva	2/0	300	8
3,000 kva	4/0	400	4
3,000 kva	2/0	300	8
3,000 kva	1	1/0	8
1,500 kva	4/0	400	2
1,500 kva	2/0	300	6
1,500 kva	1	1/0	8
1,500 kva	4	4	16



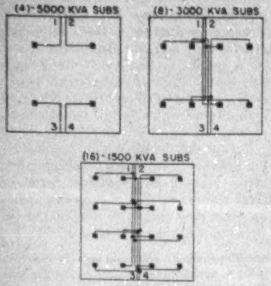


Figure 4. Substation locations and subtransmission layout for four 13.8-kv circuits supplying primary network. Shows interlacing of circuits where large number of units is used

Total Load on Each Substation	Wire Size		Circuit Miles 1,500 Kva Per Square Mile
	Overhead	Underground	
5,000 kva	4/0	400	8
3,000 kva	4/0	400	4
3,000 kva	1	1/0	16
1,500 kva	4/0	400	10
1,500 kva	4	2	20

tion in general will indicate selection of the smaller sizes, whereas, higher load densities (about 1,500 kva per square mile and more) and systems with greater degrees of duplication will indicate selection of the larger sizes.

5. Where a range of sizes is given in the foregoing cases, the use of underground cable for all or a part of the distribution feeders indicates the use of the smaller sizes of units.

6. The use of a higher subtransmission voltage tends to increase the optimum substation size, particularly where underground subtransmission is necessary.

7. For a given load area, the optimum substation size will change very little as the load density within the area increases. Generally, however, where systems with greater degrees of duplication are involved, there may be a change in this optimum size in the ratio of 1:2 in changing from 500 to 10,000 kva per square mile. This would indicate the advisability, in some cases, of starting a system with, for example, 3,000-kva units and changing the size to 5,000 kva as the load density increases.

8. As the shape of the load area departs from a square, the cost of any system using larger substations increases, thus tending to justify the use of smaller substations.

9. Regardless of the type of system, there will be a decrease in over-all system losses as smaller sizes of substations are used. Loss evaluations, however, will not affect appreciably the optimum size of substation selected only on a system-investment basis.

10. The use of the smaller substation sizes not only may result in a lower total investment in the ultimate system but also may allow the deferment of sizable blocks of system investment for as many as several years at a time and result in a considerable saving in fixed charges and carrying charges on investment. Consideration of this factor favors the use of smaller substation sizes than those determined from only a system-investment basis.

Factors Considered

The various factors involved in the design of a distribution system just listed are discussed in detail in the Appendix. Briefly, however, Figure 1 illustrates the five basic types of systems covered, and Figure 2 shows the assumed substation locations and layout of the 4.16-kv distribution circuits common to the five systems. Figures 3 and 4 are illustrative of the subtransmission circuit layouts for the basic systems. For further details, refer to the appendix.

I—System Investment

As pointed out previously, consideration of only one part of the system may lead to erroneous conclusions as to the most economical type of system or substation size to use. For example, if only substation costs are considered it will be found that as the size of the substation is increased, the "dollars investment per kilovolt-ampere of total substation load" will decrease, assuming the substations are fully loaded. This is illustrated in Figure 5 and such consideration might lead to the assumption that the larger the substation size the lower the system cost. Likewise, if reference is made only to the subtransmission circuit costs (Figure 6), the same conclusion might be reached; since as the larger substation sizes are used, the mileage of subtransmission circuits and, therefore, the cost decreases.

On the other hand, if only the 4.16-kv circuits are considered, it can be seen from Figure 7 that as smaller substation sizes are used there is a decrease in the dollars investment per kilovolt-ampere of total load carried, and therefore the conclusion might be that the smallest size substation would produce the most economical system. When, however, all three of these factors are considered collectively, an entirely different picture is obtained.

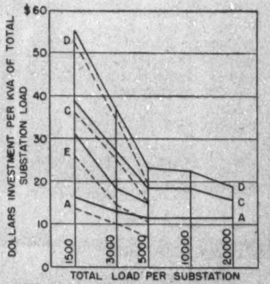


Figure 5. Investment (installed) in 13.8-kv to 4.16-kv substations to serve 20,000 kva including high-voltage switchgear (if any), transformers, low-voltage switchgear, and regulating equipment

Curve designations refer to systems of Figure 1

—— Feeder regulators and reactors

– – – Bus regulation

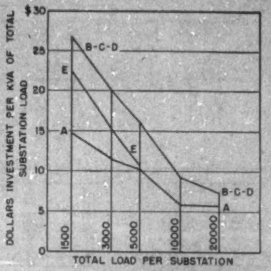


Figure 6. Investment (installed) in 13.8-kv underground subtransmission system serving area shown in Figures 2, 3, and 4 having load density of 1,500 kva per square mile

Includes cable, ducts, and manholes. Curve designations refer to systems of Figure 1

It should be noted here that as load density increases, the substation cost becomes predominant and the circuit cost, whether underground or overhead, becomes subordinate in the total system cost. Therefore, savings in length of low-voltage high-current primary distribution lines becomes smaller and shows up to a lesser degree in the over-all cost. The converse of this is true as the load density becomes lighter.

In order to determine just how the various cost elements added up, total cost figures were prepared for each of the various combinations of factors enumerated previously, such as load density, circuit voltages, and so forth. These included the estimated installed cost of all substations, subtransmission, and main primary circuits within the load area. They did not include the cost of the primary laterals, distribution transformers, or secondary circuits, although this cost is discussed. Also, the cost figures did not include real estate, operation, or maintenance. In a practical system study this latter group of costs definitely should be included, although they are not included herein because of their highly variable nature. The total-cost figures then were plotted as illustrated in Figures 9, 10, and 11.

In general, the results of this analysis show that the use of substations either too small to too large may result in excessive system investment. The use of small substations may increase considerably the length of subtransmission circuits to feed the several smaller substations. If, however, there are circuits already passing through the area it is possible that some of the stations may be tapped directly to those lines without requiring any appreciable length of new circuit. This, of course, would favor the use of smaller stations.

Figures 9, 10, and 11 by no means cover all the cases analyzed. They are sufficiently indicative, however, to illustrate the results which would be obtained in a practical case as well as the results of this method of approach. In general, these charts are self-explanatory and therefore will not be discussed further in detail. A close analysis of these charts, however, will bring out a number of additional interesting points not covered in the conclusions.

Inspection of any one of the various sets of curves shown in these charts discloses that in most cases there is a minimum point in the investment curve as the size of substations is varied. Where a minimum has not been obtained, there is usually a limitation imposed by available equipment. For example, in several cases, it will be noted that for the high-voltage–low-voltage selective system *D*, the curve is continuing a downward trend at a 20,000-kva substation. Substations larger than this were not considered since circuit breakers of adequate interrupting capacity at 4.16 kv become quite expensive.

In Figure 9, it is seen that for the simple radial *A* and primary-network *E* systems, the optimum size of substation does not change as the load density increases. On the other hand, for the other systems involving varying degrees of duplication of facilities, the optimum size of substation increases as the load density increases but the total system cost may be greater than could be obtained through the use of the primary-network or simple radial systems.

A comparison for any given set of conditions shows that as the load density increases, there is only a slight decrease in investment in those parts of the system included. This is not an entirely true picture since the cost per kilovolt-ampere of the primary laterals, distribution transformers, and secondary circuits shows a sharp decrease with increasing load density. This is shown in Figure 8 and the values thereon for the various load densities should be added directly to the corresponding set of curves in Figure 9. For example, the dollar investment scale of Figure 9 should be increased by about $55 for the 1,500 kva per square mile case, but only by about $38 for the case of 3,000 kva per square mile.

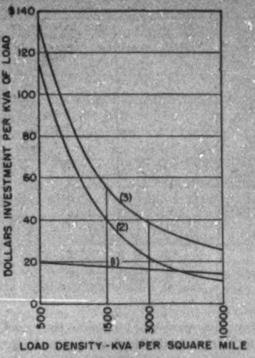


Figure 8. Distribution-system investment (installed)

Curve 1 shows investment in distribution transformers and accessories, lightning arresters, and fuse cutouts. Average peak load per transformer, 125 per cent of rating. Curve 2 shows investment in primary and secondary conductors, poles, fixtures, sectionalizing cutouts, and miscellaneous items. Service entrance conductors and meters not included. Curve 3 shows sum of curves 1 and 2

Figure 10 illustrates that, all other conditions being the same, the use of underground construction for the primary circuits dictates the use of smaller substations and the use of underground construction for the subtransmission circuits, in general, dictates the use of somewhat larger substations.

Figure 11 shows how if 34.5 kv is used for subtransmission, there is very little increase in the system cost over that obtaining if 13.8 kv is used, as long as construction is overhead, whereas, if the construction is underground this increase in cost is quite pronounced, particularly where duplicate facilities are required.

Effect of Location of Subtransmission Source

Only the subtransmission circuits within the area have been considered, but if in a practical case the source is at some distance from the area, the entire cost of these circuits should be included. For the generalized case, it can be seen readily that, if the entire length of these circuits are included, there will be no effect on the selection of the minimum economical size since any additional cost for those circuits will be constant regardless of the size of substation used.

It was assumed in the hypothetical cases that the subtransmission circuits entered from two opposite sides of the square area. If all the circuits entered from one side, there would be a slight increase in cost for certain of the cases studied, and the effect would be such as to reduce the optimum substation size.

Effect of Shape of Area

If instead of the square (4x4) shape there were assumed a long narrow (2x8) shape, there would be an increase in subtransmission costs but no change in the distribution feeder costs in all cases, except for the 20,000-kva substation. For this case, the subtransmission circuits will be the same, but there will be an increase in distribution feeder costs resulting in an increase in the over-all costs.

Comparing corresponding costs for a rectangular and square area shows that the larger sizes of substation in general will have a greater *per cent* increase in total system investment than the smaller

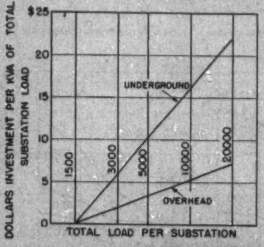


Figure 7. Investment (installed) in express feeders in 4.16-kv distribution system serving area shown in Figure 2 having load density of 1,500 kva per square mile

Curve designations refer to systems of Figure 1

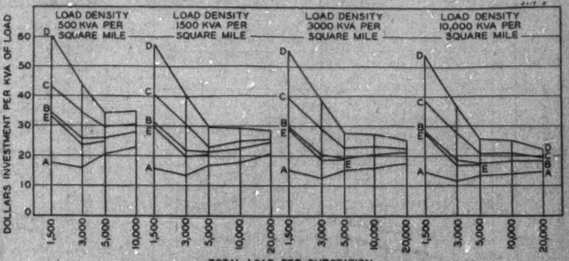


Figure 9. Effect of load density on substation size

Subtransmission is 13.8-kv overhead; primary 4.16-kv overhead. Curve designations refer to systems of Figure 1

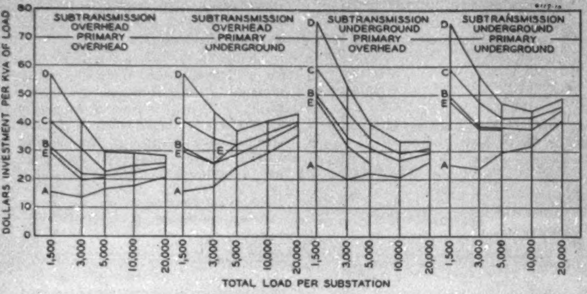


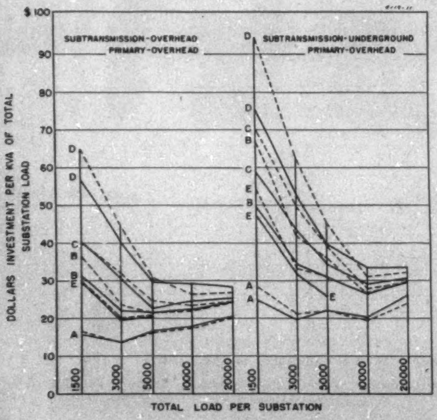
Figure 10. Effect of types of circuit construction on total system investment (installed) and size of substation

Load density 1,500 kva per square mile. Subtransmission 13.8 kv and distribution 4.16 kv. Curve designations refer to systems of Figure 1

sizes. The trend is the same whether circuit construction is overhead or underground, but is decidedly more pronounced in the latter case. Also, the trend is more pronounced where the subtransmission circuits are duplicated, or where a higher subtransmission voltage is used. Whether the subtransmission circuits enter the area from the narrow or long side, the trend is the same but the increase will be slightly greater for the former case. Practical experience in laying out a number of actual systems corroborates the fact that, as the load-area shape departs from a square, the use of smaller substation sizes results in lower cost as well as a more flexible system.

Figure 11. Effect of subtransmission-circuit voltage on size of substation and total system investment

Curve designations refer to systems of Figure 1
Primary network is overhead
Load density is 1,500 kva per square mile
—— 13.8-kva subtransmission
- - - 34.5-kva subtransmission



II—Losses

The losses for those parts of the system referred to in part I include core losses, cable dielectric losses, and all copper losses. The charge was assumed to be $100 per kilowatt for the variable losses and $350 per kilowatt for the constant losses.

Losses were considered only for the low-voltage selective (Figure 1C) and the primary network (Figure 1E) systems. Losses for the high-voltage–low-voltage selective (Figure 1D) system will be the same and for the high-voltage selective system (Figure 1B) will be only slightly higher than for the low-voltage selective (Figure 1C) system. The results for two of the cases shown in Figure 12 are typical. This part of the study shows the expected result of a decrease in losses as smaller units and higher load densities are involved.

There appears to be no appreciable difference in loss evaluation, between the two types of systems analyzed, for the same substation sizes. In general, however, the losses for the primary network are slightly less, particularly for the higher subtransmission voltages. This would indicate that regardless of the type of system assumed, smaller substations should be selected than would otherwise be the case if only system investment was considered. Regardless of this, if the loss evaluation and system investment are combined, as shown by the typical case in Figure 13, the optimum size of substation will not change from that selected on the basis of system investment. A higher evaluation of losses than used herein, though, might require modification of this conclusion for certain cases.

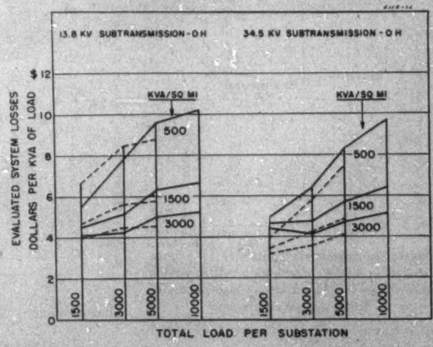
III—Deferred System Investment

The use of smaller sizes of substations allows the total installed substation capacity to follow more closely an actual load-growth curve as illustrated for three cases in Figure 14. Each of these systems has the same ultimate installed transformer and circuit capacity and at any particular time has the same load carrying ability. For the one large substation, the high-voltage–low-voltage selective system is assumed. Where 3,000-kva or 1,500-kva substations are used, a primary network is assumed.

It is readily apparent from this chart that the 1,500-kva substation will give the most flexible system from the standpoint of ability to meet changing load conditions. However, from the standpoint of system investment or operating expenses including overhead, carrying charges on investment, interest, taxes,

Figure 12. System losses

4.16-kv overhead distribution
—— Low-voltage selective (*C*) - - - Primary network (*E*)



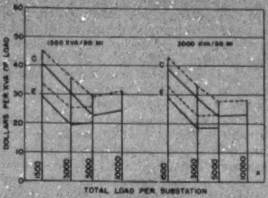


Figure 13. System losses plus total investment

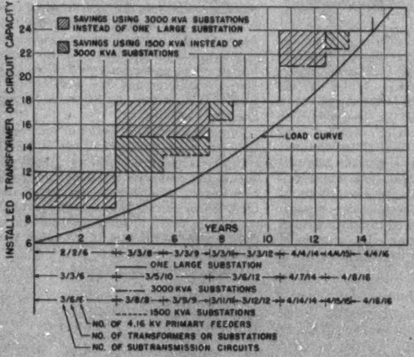
Subtransmission 13.8-kv overhead. Primary 4.16-kv overhead. Curve designations refer to systems of Figure 1

—— System investment only

– – – System investment plus evaluated losses

insurance, and so forth, the use of units rated as low as 1,500 kva may not give the maximum saving. Figure 15 shows the accumulated investment over a period of 14 years for the three systems referred to in Figure 14. From this it can be seen that the accumulated investment for a primary network system using 1,500-kva units is somewhat less than that obtained for one large substation, whereas if 3,000-kva units are used, a much larger reduction can be expected. This rather large reduction in accumulated investment is due mainly to reduction in length of subtransmission circuit and decrease in cost per kilovolt-ampere of the transformer as larger sizes are used. In this connection, practical experience has shown that a generalized study of this type tends to favor the larger substation from the standpoint of the amount of 4.16-kv feeder required. In the practical case, therefore, there very likely will be a relatively greater saving shown for the smaller substations than it is possible to show here.

Figure 14. Illustrating how use of small substations permits following load curve more closely



If carrying charges or fixed charges are considered, practical experience has shown that the best basis of comparison is to refer all these charges, to be accrued over a term of years, to "present-day" values. This gives a common basis of comparison. To illustrate the savings possible when using small substations, the charges at 12 per cent for the expenditures shown in Figure 15 were converted to "present-day" values using an interest rate of three per cent with the following results. For the system using:

(*a*). One large substation—$658,000.

(*b*). 3,000-kva units—$427,000.

(*c*). 1,500-kva units—$630,000.

Furthermore, if large substations are used and load or load growth is overestimated at the time the substation is installed, the substation equipment never may be used to its full capacity. However, the charges on the investment will continue even though the original investment is not returning its proper revenue. If the load growth is underestimated, system investment costs and, therefore, operating charges may be increased because of the difficulty of locating and building large substations economically in a short time.

Appendix

Factors Considered

1. Load Area and Density

Load densities of 500 to 10,000 kva per square mile were used. The approximate range of 500 to 3,000 kva per square mile applies generally to areas where the system types being studied would be used. The lower values apply to the more lightly loaded suburban areas and the higher values to the more heavily populated residential areas in which there also may be some commercial load.

For areas having load densities less than 500 kva per square mile it is probable that a simple radial system is the only type which could be justified. For load densities above about 3,000 kva a secondary network should be considered. Purely from a cost standpoint, secondary networks usually cannot be justified at load densities this low. However, when this value has been reached, serious consideration should be given to starting a secondary network in order to keep to a minimum the facilities to be scrapped or changed over when the higher load densities actually are obtained. Although the cases will be rare, a load density of 10,000 kva per square mile was studied since it is possible to find such areas wherein it would not be possible to justify a secondary network.

For the basic study a large square load area was assumed. This was divided into 16 smaller square areas each of which was large enough to pick up approximately 1,500 kva of load, an average loading for a 4.16-kv feeder. All loads were assumed to be uniformly distributed over the area with the load center at the center of the area and the load uniformly distributed along the laterals and branches of the distribution system.

2. Circuit Voltages

In all cases the primary distribution voltage was assumed to be 4.16 kv, since it is the predominant distribution voltage. The use of a higher or lower voltage would tend to result in optimum substation sizes larger or smaller, respectively, than those determined by this study.

Subtransmission circuits of 13.8 kv and 34.5 kv were selected, since it was felt that,

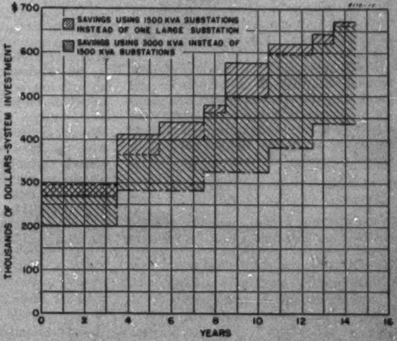
Figure 15. Illustrating how use of small substations makes possible savings by deferment of system investment

Investment values are based on curves of Figure 14 and include subtransmission-source switching equipment, subtransmission lines from source three miles from edge of area, substations, and main primary circuits, but do not include primary laterals, distribution transformers, secondaries, service entrances, or meters

—— One large substation

—.— 3,000-kva substation

– – – 1,500-kva substation



in the majority of cases, voltages falling within these limits would be used to supply substations stepping down to 4.16 kv.

3. Voltage Conditions

The 4.16-kv system was designed so that the voltage difference between the first and last transformer within any substation area would not exceed about three per cent. From a voltage standpoint the worst case considered was for a load density of 500 kva per square mile, where one 3,000-kva substation having bus regulation was used to serve a total of six square miles. In this case, the requirement of three per cent voltage difference was not met completely. In a practical case, however, it probably would be cheaper to use bus regulation and small pole-mounted regulators to obtain the proper voltage conditions than it would be to use feeder regulators at the station. For all other conditions, this voltage requirement was met and in some cases was actually less than three per cent. This reduction is inherent, however, with the particular system design and does not represent any increase in system investment.

It was assumed that from the load center of each of the small areas there would radiate a few medium-size wires (1/0) and to them would be attached smaller (number 2) branch or lateral wires. Distribution transformers were connected to both of these.

All voltage-drop calculations were made on the basis of normal peak-load conditions at 0.9 power factor with all feeders in operation. Under an emergency causing overloads on any portions of the system, it was assumed that it would be allowable to let the voltage sag.

The distribution system within the load area (including laterals, transformers, and secondary circuits) will remain the same for all cases involving the same load density, regardless of size of substation or type of system used, if it is assumed that regulating equipment is available to maintain approximately rated voltage at the load center of the area. Therefore, when comparing various system designs on a common load-density basis the cost of the distribution system within the individual load areas was not included. When studying the cost of any specific system as the load density within the given area increases, it would be necessary, since there would be a very sharp decrease in cost per kilovolt-ampere of load as the load density increased, to include the costs of the primary laterals, transformers, and secondaries. This is shown quite clearly in Figure 8.

It should be noted that the three per cent used in this analysis is not necessarily rigid but is a figure commonly used in the industry. The use of a higher or lower value merely would decrease or increase, respectively, the investment of that part of the system referred to in Figure 8.

4. Service Continuity—Types of Systems

The degree of service continuity actually obtained on any one system is directly affected by the degree to which the various parts are duplicated. Local conditions as to customer's requirements for continuity of service, line construction through congested areas, and so forth, all contribute to the fact that in actual practice there are quite widely varying ideas and practices as to the actual requirements for duplication of service. In order to determine the extent to which duplication of the various portions of the system contribute to the over-all investment five basic types of systems with various degrees of duplication were studied. These systems are illustrated in Figure 1.

The primary network represents the highest degree of continuity of service which can be obtained since even a low voltage bus failure will not cause an outage on the primary distribution circuits.

Figure 2 shows the only portions of 4.16-kv circuits included in the basic part of this analysis. This layout is the same for each of the five types of systems analyzed, and the feeders may be either overhead or underground as assumed in the study. All other parts of the circuits not shown in Figure 2 are assumed to be overhead.

Figures 3 and 4 show layouts typical of the subtransmission circuits made for each of the systems of Figure 1. In all but one case, four 13.8-kv and three 34.5-kv circuits were used. For the simple radial system (*A*) only two 34.5-kv feeders were required to carry the normal load where no overloads could be incurred. It was assumed that each 13.8-kv circuit could be loaded to about 8,000 kva and each 34.5-kv circuit to about 12,000 kva under emergency conditions with one circuit being out of service.

For the primary network the interconnections between substations could be made by tying together the laterals which were assumed to radiate from the centers of the load areas. As pointed out previously, these laterals were assumed to be 1/0 conductors. In this case, however, 4/0 conductors were assumed with the length of circuit remaining the same and the proper cost applied. In actual practice, some rearrangement and possible additional lengths of circuits may be required in order to make these connections between stations, but, in general, these should not be enough to be significant.

5. Substation Size and Equipment Ratings

Various sizes of substations to serve the load area were assumed as shown in Figure 2. Where no duplication of transformer capacity was assumed (systems *A* and *B*), the kilovolt-ampere ratings of the transformers are as shown in Figure 2. In those cases where spare transformer capacity was available (systems *C*, *D*, and *E*) transformers were selected having a normal kilovolt-ampere rating such that with one transformer or subtransmission circuit out of service, the remaining transformer or transformers would be required to carry a load of 1.35 times normal rating. In determining the substation loads, a diversity factor of 1.2 was used where four or more feeders were involved. Although lower diversity factor would require a greater substation capacity, fans for forced-air cooling of the transformers to obtain greater load carrying ability could be installed for negligible additional cost.

If the subtransmission circuits for the primary network are interlaced to the maximum degree (Figure 4) the minimum overload (about 35 per cent) will be imposed on the other substations with one line or transformer out of service. With a lesser degree of interlacing the overloads, in general, will exceed 35 per cent and require larger normal ratings of substations just to carry the overloads. This actually may result in a more economical system however, since subtransmission circuits may be quite costly, particularly if of underground construction, whereas the incremental transformer cost is quite low.

Circuit breakers of adequate interrupting ability were selected on the assumption that there would be available approximately 125,000 kva of short-circuit capacity per 13.8-kv feeder and approximately 250,000 kva of short-circuit capacity per 34.5-kv feeder. Feeder circuit breakers were selected on the basis of automatic reclosing.

In all substations required to carry a normal load of 5,000 kva or more, involving four or more feeder positions from one bus, individual feeder regulation was assumed. The reason for this is that where approximately four or more feeders are connected to the same bus, the diversity in loading between feeders is generally such that it would not be possible to maintan proper voltage conditions within the substation area if bus regulation were used. Bus regulation was incorporated only in substations designed to carry 3,000 kva of load or less involving two feeders or less.

In a practical case, there may be some question as to the ability to maintain good voltage conditions on even two feeders with bus regulation without some means of supplementary regulation, particularly at the lighter load densities. On the other hand it is entirely possible that a part of the range of voltage control required could be incorporated as bus regulation in some of the larger sizes of substations, and individual feeder regulators could be used for some or all of the feeders; these regulators having something less than the total required range of control. For such cases, the over-all cost figures might indicate an increase in the optimum substation size. At the higher load densities, particularly where the 4.16-kv system is underground, it is possible that as many as four or more feeders could be regulated from one bus.

In all cases whether feeder or bus regulation was used, sufficient range of voltage control was included to maintain rated voltage at the center of the individual feeder load areas. Where feeder regulators were used, it was necessary to assume the use of reactors in order to limit the short-circuit duty on the regulators to 25 times their normal current rating.

Load-Calculation Procedure for Electric-Panel Space Heating

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THE use of radiation as a means of establishing comfort conditions in an occupied space is not a new idea to the electrical engineer. Small incandescent electric heaters have long been used as radiant sources capable of providing spot heating with a minimum time lag and of achieving a locally comfortable environment in a space where the air temperature is lower than 70 degrees Fahrenheit. With respect to both of these characteristics such heating units resemble the open fireplace and are subject to its disadvantages as well as its advantages. On the negative side must be considered the high intensity of the source with consequent difficulty in obtaining a satisfactory uniformity of heating effect.

Radiant panel heating is, basically, an adaptation of the incandescent source method, but it is designed largely to overcome the disadvantages of the latter system while retaining, in large measure, its desirable features. A heating panel can be visualized as a radiant source of large area operating at low temperature; thus it can be distributed throughout the occupied space in a manner which will provide uniform irradiation of the occupants without hazard of exposure to high local intensities.

Aside from its attraction as a possible means of strengthening the competitive position of electricity as a space-heating energy source, panel heating affords specific advantages over convective systems which have led to its rapid adoption both in the United States and throughout Europe. In the ten years preceding the war over 3,000 installations—of all types—were made in European countries, while the growth of interest in this country is indicated by the fact that there was a total of five installations in 1938 and close to 200 by 1941. This rapid growth of interest suggests the possibility of striking and rapid developments in the panel method following the war.

The advantages claimed for panel heating come under the three general headings of comfort, aesthetics, and economy. The reduced air temperature frequently realized with a panel system (ten degrees Fahrenheit for the example worked in this paper) leads to a greater feeling of "freshness," provides a higher indoor relative humidity (corresponding to a fixed absolute humidity), and reduces the shock effect—with its accompanying health hazard. This occurs when a sudden temperature change takes place in the inhaled air passing over the mucous membranes of the nose as when one enters or leaves a heated space. Aesthetically, panels have a marked architectural advantage, as they can be made an integral part of the structure, and thus do not occupy space otherwise useful nor interfere with the decorative scheme. In most installations low-temperature panels form an integral part of the ceiling and are subject to the same decorative treatment as the unheated part; they are therefore invisible. From the standpoint of operating economy, the lower air temperature means a reduced energy requirement for heating the ventilation air. For spaces having a high ventilation rate (over five air changes per hour), this factor will lead to a very real reduction in operating cost. Transmission losses vary with the type of structure and may be either greater or less than for convective heating. In general, however, data from existing installations suggest that, on the average, an over-all operating saving of not less than ten per cent can be expected. Based on experience with some 500 multistory installations English contractors will guarantee a 30 per cent saving, but in terms of American experience this figure appears unduly optimistic.

Electricity as a Panel Energy Source

On a direct Btu basis, electricity cannot compete easily with other energy sources for the purpose of space heating. To improve its competitive position, means therefore must be found for making one unit of electric energy provide a heating effect in excess of that obtained from an equal quantity of energy in some other form. One method of doing this is to use a heat pump, thereby obtaining four or five kilowatts of heating effect for each kilowatt expended in electric power. Another method is to accomplish the objective of space heating without actually raising the temperature of the space to the usually accepted value. Radiant heating—by offsetting normal body heat loss with a direct irradiation component—permits realization of optimum comfort in a space having a reduced air temperature and thereby accomplishes space "heating" with a total energy input which is usually substantially less than that needed for an equivalent convective system of conventional design.

Further, the surface temperature of space-heating panels rarely exceeds 100 to 120 degrees Fahrenheit, so that, even in installations where sinuous coils are used with hot water as the heating medium, the first cost of thermal storage is reduced to a point which strengthens the competitive position of electricity as a primary energy source. If 210 degrees Fahrenheit is taken as the maximum storage temperature in a system without hydrostatic head, the size of the thermal storage tanks for a panel system with 100 degrees Fahrenheit circulating fluid would be less than 30 per cent as great as would be required for a radiator system circulating hot water at 180 degrees Fahrenheit. Thus with panel heating electric thermal storage can be installed at greatly reduced first cost. The total energy requirement for such a system is, of course, no less than for direct electric panels, but the opportunity of utilizing off-peak energy *exclusively* will permit application of a more favorable rate schedule and elimination of the demand charge. Over 250,000 kw of electric thermal-storage systems are now installed, including systems having seasonal power requirements in excess of 2,000,000 kilowatt-hours. As a first approximation 200 kw can be taken as the minimum size of economically feasible thermal-storage system.

Off-peak electric thermal storage is equally as attractive to the utility as it is to the consumer. With electric boilers operating only during the night hours, such systems afford a favorable load leading to an improved station load factor. When indirect thermal storage is considered in conjunction with use of an electrically operated heat pump, the optimum condition of a nonseasonal 100 per cent off-peak load appears as a distinct possibility. Considering also that the lower permissible winter surface temperature and higher summer surface temperature of the panel system give a distinctly higher coefficient of performance than could be obtained from a heat pump operating on a conventional all-year conditioning system, the very favorable position of electricity as the energy source becomes evident. Three multistory buildings have been equipped with all-year panel systems, but none of these utilizes either a heat pump or thermal storage.

When the first cost of a heat pump, of thermal storage, or of stored cooling

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effect is being estimated, the reduced-operating-temperature range of the panel system must be given consideration, as the size of equipment for all three of these purposes is likely to be much less than otherwise would be needed.

European practice has been to use immersion-heated electric boilers for loads up to 400 kw and electrode type—operating at around 10,000 volts—for greater loads. The "Underground" Administration Building in London uses two immersion-heated boilers to raise 16,000 gallons of water to 270 degrees Fahrenheit (corresponding to the hydrostatic head of the ten-story building); the boilers operate for the 12-hour period starting at 7 p.m. and store 23,000,000 Btu for use during the subsequent 12-hour period during which no electric energy is received.

Types of Electric-Heating Panels

All of the preceding discussion has been with respect to low-temperature space-heating panels. While units of this type constitute the field of greatest interest and afford the greatest potential electric load, nonetheless completeness requires a brief mention of the other two classifications of electrically heated panels that are now in use. These are:

1. *High-Temperature Units.* Detachable "package"-type unit panels designed for auxiliary service are used widely in Europe. Many methods of construction are employed, but the average panel of this type has a rating of from 250 to 500 watts per square foot and operates with a surface temperature between 300 and 600 degrees Fahrenheit. Usually resistance wiring is embedded in some form of baked-clay slab, the heating surface being flat and of a dark color. Because of the relatively high intensity, panels of this kind are likely to cause serious nonuniformity in heating effect and may cause discomfort to the occupants if used in a room having a ceiling height of less than ten feet. Their principal application is in establishing local zones of comfort within a large working space—as a factory or large garage—in which the general temperature level is too low for comfort.

2. *Medium-Temperature Units.* These panels are also of the detachable type, are subject to the same limitations as high temperature units, and have the same usefulness as auxiliary localized heat sources. Average rating is from 80 to 180 watts per square foot with a surface temperature between 150 and 250 degrees Fahrenheit. One commonly used unit of this type consists of a blackened sheet-metal radiating surface backed with resistance wiring on an insulating base.

Many varieties of panels of the low-temperature space-heating type have been used, and a number of kinds has gained extensive practical application. The average surface temperature is between 95 and 100 degrees Fahrenheit with a rating of 15 to 20 watts per square foot. One panel of this type which has been used in close to 200 installations in England consists of a thin ($^1/_{16}$-inch) dielectric fabric with integral resistance wiring rated at one watt per lineal foot of wire and 16 watts per square foot of panel. The fabric is made in rolls up to 100 feet in length and of either 20 or 40 inches wide. Each two-foot section is heated by a single nickel–copper resistance wire connected at the sides of the fabric to stranded copper bus bars which are looped at intervals of two feet. This permits cutting the fabric to any desired multiple of two feet. After the fabric is cut, the tabs formed by the broken sides of the bus-bar loops are extracted from the fabric and used to connect to the source of power. The very low load on the resistance wiring is said to provide ample protection against failure, but if a wire does fail it makes inoperative a two foot length of panel. The largest installation of this type is rated at 300 kw. In passing, it is interesting to note that the first-class lounge on the *Queen Mary* is so equipped.

A complete electric-fabric-panel system of the type described has been installed in one of the experimental air-conditioning rooms at the University of California and is now being studied as one project of a comprehensive research program on radiant panel heating.

Analogy Between Lighting and Panel Heating

The fundamental objective in the design of a panel-heating system is to provide a uniform irradiation of the occupants sufficient to compensate for the greater convective heat loss which they experience as a result of lowered air temperature. While the design problem is basically one of establishing a heat balance, it is in many respects surprisingly analogous to the type of problem with which the electrical engineer comes in contact when designing a lighting system. Both problems are concerned with radiant transfer, and, though the section of the spectroradiometric curve, which is important to panel heating, occurs at a much longer range of wave lengths than that for light, the mechanism of transmission and the reflection and distribution problems associated with the energy transfer are the same in each case.

In illumination problems the designer must provide a sufficiently powerful light source to maintain the requisite illumination on the working plane and must distribute the luminaires in such a manner as to avoid glare, bright spots, and shadows. The panel designer has analogous problems in that he must provide a sufficiently powerful heat (infrared-radiation) source to maintain the requisite irradiation in the occupied region and must distribute the panels in such a manner as to avoid high-intensity "hot" spots and "cold" shadows. The remarkable similarity between illumination and panel heating is particularly helpful to the electrical engineer as it permits him to apply to panel design most of the equations and all of the shape-factor data which are included in the standard illuminating-engineering texts and references.

With respect to the distribution problem, panel design requires more data and a more complex treatment than is needed in illumination. In lighting design the problem is to provide uniform illumination on a flat working plane while with radiant heating the objective is to uniformly irradiate a human of varying and complex surface as he moves with respect to the fixed source. The analogy between the two types of problem can be extended however to include the typical case of providing a local region of greater illumination (or infrared irradiation) in a large enclosure for which the general level of illumination (or heating) is inadequate. The similarity between these two problems is of even greater interest when one remembers that the *need* for a greater intensity of localized lighting is likely to occur at the same point for which greater heating will be needed; thus the possibility of developing electric panel heating to correlate with the lighting is immediately suggested.

The use of properly selected wall-surfacing materials and colors can increase greatly the efficiency of a lighting system; similarly, the use of surfacing materials having a high reflectivity for infrared radiation will improve the distribution in a panel-heated room and—if materials of very high reflectivity were commercially available—would reduce the required energy input. Care must be exercised, however, in applying the reflection analogy to panel heating. Materials which have a very high reflectivity when irradiated with energy having the wave-length range of light may be almost completely ineffective as reflectors of infra-red-heating rays. Thus silver-backed mirror walls would improve lighting efficiency but would not better the efficiency of a panel-heating system appreciably.

One additional characteristic of lighting systems which is theoretically possible with panel heating, but which no commercial system has yet achieved, is immediacy of response. If an electrically heated panel could be developed having inappreciable thermal capacity the surface temperature would be raised as soon as the panel was energized. The desirability of such a system needs no comment, but no satisfactory method has so far been developed for reducing the thermal capacity and hence obviating the lag in response that is found with existing panels. As this difficulty is overcome, more rapid and precise control of all-year panel systems will follow, and the energy loss associated with the passing of heat into or out of storage in the panel will be reduced.

Panel-Design Procedure

THE DESIGN PROBLEM

Basically, the first step in the design of a radiant-heating system is to determine the amount of panel surface required and the temperature at which it must operate; in most installations the maximum allowable panel temperature is a design constant, and the problem therefore resolves itself to the determination of the necessary area of panel. Once the panel area is fixed, the second design step is to distribute that area throughout the heated space in such a manner as to provide uniformity of heating effect for all positions which the occupants may take within the enclosure.

The first two steps in the design procedure, as previously listed, are completely independent of the type of panel used and of the heating medium; they are the same for electric, steam, hot water, or direct gas-fired panels. These also are the most difficult steps and the ones which can most readily be responsible for an ineffective or inoperative system. The first step can be carried out by a rational analysis which, though not simple, is yet straightforward. Since this part of the design is of the greatest importance and is basic to all else, a detailed analytical procedure accompanied by a representative numerical example will be presented in a following section.

The second design step, distribution analysis, does not admit of a rational treatment, but can be carried out either in terms of experience with other similar installations or by application of the fundamental experimental data, available in the literature,[4] on the shape factor of a person with respect to a panel. Standard ceiling-panel patterns have been recommended[4] for different ceiling heights, and it is suggested that these be used or that a complete graphical analysis of the particular installation be made in terms of the experimental shape-factor curves. This step will not be considered further in this paper.

The third and last design step—developing a type of panel which will permit bringing the requisite amount of energy from the source to the panel surface—is essentially a problem of commercial design. Innumerable methods have been used, the procedure varying with the panel temperature and with the medium used for heating. In electrically energized panels the principal problems are to provide sufficient facing material to insure adequate heat diffusion and consequent uniformity of panel-surface temperature, and yet not to allow the thermal capacity of the panel to become great enough to cause delayed response to load changes. This entire problem is one involving highly competitive commercial design and will not be considered further in this paper.

In the sections which follow, the complete analytical procedure will be developed for carrying out the rational design of a panel-heating system. Equations will be established for evaluating the surface temperatures throughout a radiant-heated room for equilibrium conditions, the necessary temperature of a panel of fixed area will be calculated and, the required maximum energy input of the heating plant will be determined. Since ceiling panels are much more effective in transferring a large fraction of energy by radiation than are either wall or floor panels, the analysis will be restricted to units of this type; extension of the procedure to either wall or floor panels is simple, however, as it requires merely an adjustment in the values of the film coefficients of heat transfer.

The rational procedure involves setting up a sufficient number of independent heat-balance equations to permit a simultaneous solution leading to evaluation of the air temperature and of all surface temperatures within the heated enclosure. This is done by writing balance equations on the ventilation air and on each unheated type of surface which exists in the room; a last equation, determined empirically from considerations of individual comfort, permits simultaneous solution of the group. Since the basis of the entire procedure is the equation for a heat balance on a surface, this equation will be developed and discussed in detail, in the section immediately following.

HEAT BALANCE ON A UNIPLANAR SURFACE

To establish a heat balance on a surface subject to steady-state energy exchange, the net heat flow to the surface from the surround on one side must be equal to the net heat loss to the surround on the other. Since the mean radiant temperature (average weighted inside surface temperature) of a panel-heated room exceeds the inside air temperature, a reasonable starting assumption is that the inside surface receives energy by radiation from its enclosing surfaces, loses energy by convection to the inside air, and loses energy by conduction through the solid material (as wall, floor, ceiling, or glass) of which the surface in question is the inside face. If this assumption is incorrect, the results of the analysis still will be valid, provided only that the assumed temperature relationships are used consistently when establishing heat balances on all surfaces of the enclosure.

If we consider a surface, *s*, for which the balance is to be written and evaluate the net radiant transfer to *s* from any other surface, 1, which *s* "sees," the basic radiation equation[1] is

$$q_{R_{1s}}=\sigma\left[e_1\int_{A_1}\int_{A_s}\frac{T_1^4\cos\phi_1\cos\phi_s dA_1dA_s}{\pi r^2}-e_s\int_{A_s}\int_{A_1}\frac{T_s^4\cos\phi_1\cos\phi_s dA_1dA_s}{\pi r^2}\right]\quad(1)$$

where

$q_{R_{1s}}$ = net direct radiant transfer (q, Btu per hour) from surface 1 to surface s

σ = Stefan-Baltzman constant = 0.172×10^{-8}

T = temperature, degrees Fahrenheit absolute, at a point on the surface

e = emissivity

r = distance between centers of the two surfaces

ϕ = angle between line r and a normal to the surface

A = area, square feet

Because of change of distance and position of each point of one surface with respect to any other surface, the temperature is a point function. In practice, however, but small error is introduced into the analysis by considering that the variation of temperature from point to point, on a given surface, is negligible. Further simplification can be realized if the emissivities of the source and receiver are the same. Introducing these conditions into equation 1 gives

$$q_{R_{1s}}=e\sigma(T_1^4-T_s^4)\times\int_{A_1}\int_{A_s}\frac{\cos\phi_1\cos\phi_s\,dA_1\,dA_s}{\pi r^2}\quad(2)$$

The fundamental weakness of equation 2 is that it evaluates only that fraction of the total radiant exchange from 1 to *s* which is due to direct exchange of energy emitted by these two surfaces. Additional transfer will occur as a result of interreflections from other surfaces forming the enclosure and from rereflections between 1 and *s* of energy originally emitted by 1 and eventually absorbed by *s*. Precise evaluation of reflected component of radiant exchange is complex and time-consuming.[3] An approximate method of correcting equation 2 is to treat the transfer surfaces as though they were perfect emitters and perfect receivers: that is, to assume e = 1.0. For surfaces of the type usually found in panel-heated spaces, this assumption is well justified, as the actual emissivities are likely to be greater than 0.9, and, since the transfer surfaces form part of an enclosure, the effective emissivity—due to interreflections and rereflections—will exceed the actual value.

Further simplification of equation 2 is obtained by noting that the term under the double integral is a function only of the geometry of the system and can be evaluated in terms of the shape, size, and relative position of the two surfaces. By definition, the shape factor of surface *s* with respect to 1 is

$$F_{s1}=\frac{1}{A_1}\int_{A_1}\int_{A_s}\frac{\cos\phi_1\cos\phi_s\,dA_1\,dA_s}{\pi r^2}\quad(3)$$

and is numerically equal to the fraction of energy received at *s* of that emitted by 1. Similarly F_{1s} can be evaluated, and, by inspection of the defining equations, the reciprocity theorem is obtained:

$$F_{s1} A_1 = F_{1s} A_s \tag{4}$$

In terms of shape factor and for bodies which are perfect emitters and receivers, equation 2 now becomes

$$q_{R_{1s}} = \sigma(T_1^4 - T_s^4) F_{1s} A_s \tag{5}$$

A disadvantage of this equation is the fourth-power exponent on the temperatures. Basically, the exponential relationship is needed for a rational expression, but a satisfactory first-power expression—adequate for panel-heating problems—can be obtained by noting that in the usual range of temperatures

$$\frac{\sigma(T_1^4 - T_s^4)}{T_1 - T_s} \doteq 1.08$$

and this permits rewriting equation 5:

$$q_{R_{1s}} = 1.08(t_1 - t_s) F_{1s} A_s \tag{6}$$

where t is the surface temperature in degrees Fahrenheit.

The net radiant transfer to s from all of the different surfaces constituting the enclosure is then

$$\begin{aligned} q_{R_s} &= q_{R_{1s}} + q_{R_{2s}} + \ldots + q_{R_{ns}} \\ &= 1.08 A_s [F_{1s}(t_1 - t_s) + F_{2s}(t_2 - t_s) + \\ &\qquad \ldots + F_{ns}(t_n - t_s)] \end{aligned} \tag{7}$$

The loss of energy from s to the room air can be evaluated by Newton's law of cooling:

$$q_{V_s} = h_s(t_s - t_a) A_s \tag{8}$$

where q_V is the convective heat loss, Btu per hour and h is the film coefficient of heat transfer, Btu per hour (square feet)(degrees Fahrenheit).

Heat loss from the surface through the structural material to the outside air is given by the equation:

$$q_{C_s} = C_s A_s (t_s - t_o) \tag{9}$$

where t_o is the outside air temperature and C_s is the conductance measured from surface s to the outside air. In most problems the over-all coefficient of heat transfer, U (from inside air to outside air) is known or can be obtained directly from a handbook; in that event the conductance can be calculated from the equation:

$$C = \frac{1}{\frac{1}{U} - \frac{1}{h_s}} \tag{10}$$

Now if we write the heat-balance equation[1] for surface s,

$$q_{R_s} = q_{V_s} + q_{C_s}$$

or substitute from equations, 7, 8, 9,

$$\begin{aligned} 1.08[F_{1s}(t_1 - t_s) + \ldots + F_{ns}(t_n - t_s)] \\ = h_s(t_s - t_a) + C_s(t_s - t_o) \end{aligned} \tag{11}$$

and

$$t_s = \frac{1.08[F_{1s}t_1 + \ldots + F_{ns}t_n] + h_s t_a + C_s t_o}{1.08(F_{1s} + \ldots + F_{ns}) + h_s + C_s} \tag{12}$$

Equation 12 is the fundamental heat-balance equation which is used as a basis of the analytical procedure for panel design. For all practical problems, however, it can be further simplified to one of two special forms. If surface s is uniplanar, the sum of the shape factors appearing in the denominator must be equal to unity (since 100 per cent of energy emitted by s is received by the enclosure); so equation 12 reduces to

$$t_s = \frac{1.08(F_{1s}t_1 + \ldots + F_{ns}t_n) + h_s t_a + C_s t_o}{1.08 + h_s + C_s} \tag{12a}$$

while if s is not uniplanar it possesses a shape factor with respect to itself, F_{ss}, and the sum of shape factors,

$$F_{1s} + \ldots + F_{ns} = l - F_{ss}$$

and so

$$t_s = \frac{1.08(F_{1s}t_1 + \ldots + F_{ns}t_n) + h_s t_s + C_s t_o}{1.08(1 - F_{ss}) + h_s + C_s} \tag{12b}$$

Equations 12*a* and 12*b*, with minor variations, provide the prototypes for all of the surface heat-balance equations required in the panel-design procedure.

HEAT BALANCE ON A PANEL-HEATED ROOM

In the previous section an equation was established which gives the temperature of a surface as a function of the temperatures of all surrounding surfaces, the inside air and the outside air. Thus, for a problem in which only the outside-air temperature is known, numerical determination of any surface temperature requires simultaneous solution of a set of independent heat-balance equations, the number of equations exceeding by one the number of different surfaces making up the enclosure. One independent equation can be written for each type of surface except the heating panel, and an equation likewise can be written for the ventilation air as it passes through the room.

There remains, then, need for one additional equation. This last equation can be established from a consideration of the relationship which must exist between the inside-air temperature and the weighted average inside-surface temperature (mean radiant temperature) if comfort conditions are to exist within the room. Usually, differences in surface temperatures are not great, and the mean radiant temperature can be evaluated approximately as the average surface temperature. Since adequate and comprehensive experimental data are lacking, it is suggested that the comfort relationship be based on the condition that a one-degree-Fahrenheit change in mean radiant temperature is compensated for by a one-degree-Fahrenheit change, opposite in direction, of the inside-air temperature. Considering also that comfort would be realized in a room with air and walls at 70 degrees Fahrenheit, the comfort equation can then be written:

$$t_a = 140 - \frac{t_f A_f + t_i A_i + t_g A_g + t_w A_w + t_p A_p}{A_f + A_i + A_g + A_w + A_p} \tag{13}$$

where the subscripts f, i, g, w, p represent floor, inside partition, window, wall, and panel surfaces. As written, equation 13 is for a room in which the entire ceiling is occupied by the heating panel; if this condition were not met (as, usually, it would not be) an additional term would be needed to represent the unheated ceiling surface.

The equation for a heat balance on the ventilation air can be written by considering that W pounds per hour of outside air are heated to room temperature as a result of convection from the inside surfaces:

$$\begin{aligned} 0.24W(t_a - t_o) = h_p(t_p - t_a)A_p + \\ h_g(t_g - t_a)A_g + h_w(t_w - t_a)A_w + \\ h_f(t_f - t_a)A_f + h_i(t_i - t_a)A_i \end{aligned}$$

or

$$t_a = \frac{\begin{aligned} h_p t_p A_p + h_g t_g A_g = h_w t_w A_w = \\ h_f t_f A_f = h_i t_i A_i + 0.24Wt_o \end{aligned}}{h_p A_p + h_g A_g = h_w A_w = h_f A_f + h_i A_i + 0.24W} \tag{14}$$

For a room having two adjoining exterior walls, each containing window area, and a ceiling entirely covered with panel, the surface heat-balance equations would be (by analogy with equations 12*a* and 12*b* and with subscripts as previously defined)

$$t_f = \frac{1.08(F_{if}t_i + F_{fp}t_p + F_{gf}t_g + F_{wf}t_w) + h_f t_a + C_f t_o}{1.08 + h_f + C_f} \tag{15}$$

$$t_w = \frac{\begin{aligned} 1.08(F_{fw}t_f + F_{iw}t_i + F_{pw}t_p + F_{gw}t_g) \\ + h_{wa}t + C_w t_o \end{aligned}}{1.08(1 - F_{ww}) + h_w + C_w} \tag{16}$$

$$t_g = \frac{1.08(F_{wg}t_w + F_{fg}t_f + F_{ig}t_i + F_{pg}t_p) + h_g t_a + C_g t_o}{1.08(1 - F_{gg}) + h_g + C_g} \tag{17}$$

$$t_i = \frac{1.08(F_{gi}t_g + F_{wi}t_w + F_{fi}t_f + F_{pi}t_p) + h_i t_a + C_i t_o}{1.08(1 - F_{ii}) + h_i + C_i} \tag{18}$$

Six simultaneous equations now have been established (13, 14, 15, 16, 17, 18) the solution of which will determine the six unknown temperatures (t_a, t_p, t_i, t_g, t_w, t_f) thereby fixing the necessary panel temperature and permitting calculation of the required energy input to the system. In many problems the panel temperature is fixed as a design constant in which case the panel area, A_p replaces t_p as an unknown. Note, however, that for this case there must be seven equations and seven unknowns as some unheated ceiling surface, at unknown temperature, will exist. The area of unheated ceiling likewise is unknown, but can be readily expressed as the total ceiling area minus the panel area.

NUMERICAL EXAMPLE

To show the application of the fundamental design equations to a particular problem consider a room 20 by 20 by 10 feet having two inside walls ($U = 0$), two adjoining exterior walls, ($U = 0.25$) each with a window ($U = 1.13$) 10 feet long by 5.68 feet high, the sill $2^1/_2$ feet above the floor, and the vertical center line of the window located on the center line of the wall. The floor ($U = 0.25$) is exposed to outside air. Outside-air temperature will be taken as zero degrees Fahrenheit, and ventilation at a rate of one air change per hour (4,000 cubic feet per hour giving $W = 297$ pounds per hour). The entire ceiling consists of the heating panel. Film coefficients can be taken as 0.4 for the panel; 0.7 for walls, partitions, and windows; 1.1 for the floor. The constants needed in solving the simultaneous equations are then

$$h_p=0.4 \quad h_w=h_g=h_i=0.7 \quad h_f=1.1 \quad W=297$$

$$C_w=\cfrac{1}{\cfrac{1}{U_w}-\cfrac{1}{(h_W+h_R)}}=\cfrac{1}{\cfrac{1}{0.25}-\cfrac{1}{0.7+1.08}}=0.291 \quad C_i=0$$

$$C_f=\cfrac{1}{\cfrac{1}{0.25}-\cfrac{1}{1.1+1.08}}=0.283$$

$$C_g=\cfrac{1}{\cfrac{1}{1.13}-\cfrac{1}{0.7+1.08}}=3.09$$

$$A_p=400 \quad A_f=400 \quad A_i=400 \quad A_g=113.6 \quad Aw=286.4$$

Determination of the shape factors can be accomplished analytically, but the computations are involved and tedious. In the present case all surfaces are square or rectangular and are in planes either parallel or normal to one another. Graphical solutions for problems of this type are available in standard references,[2,3] and the numerical values tabulated in the following therefore have been obtained either directly or by interpolation from one or the other of these sources (or by application of equation 4):

$F_{fi}=0.300 \qquad F_{if}=0.300$

$F_{fp}=0.41 \qquad F_{pf}=0.41$

$F_{fw}=0.326 \qquad F_{wf}=0.234$

$F_{fg}=0.304 \qquad F_{gf}=0.086$

$F_{ii}=0.150$

$F_{ip}=0.300 \qquad F_{pi}=0.300$

$F_{ig}=0.253 \qquad F_{gi}=0.072$

$F_{iw}=0.228 \qquad F_{wi}=0.178$

$F_{ww}=0.108$

$F_{wp}=0.208 \qquad F_{pw}=0.290$

$F_{wg}=0.080 \qquad F_{gw}=0.032$

$F_{gg}=0.043$

$F_{gp}=0.092 \qquad F_{pg}=0.325$

A check on the accuracy of the shape factors can be obtained by noting that, for any surface, the sum should be equal to unity:

$$F_{fw}+F_{iw}+F_{pw}+F_{gw}+F_{ww}=1$$
$$0.326+0.228+0.290+0.032+0.108=0.984 \quad \text{(Error}=1.6\text{ per cent)}$$

Similar check on the other surfaces will show the error is in no case greater than five per cent.

Determination of Equilibrium Temperature. Substituting the known terms into the six simultaneous equations gives:
From equation 13,

$$t_a=140-\frac{400t_f+400t_i+113.6t_g+286.4t_w+400t_p}{400+400+113.6+286.4+400}$$

$$t_a=140-0.25t_f-0.25t_i-0.25t_p-0.071t_g-0.179t_w \qquad (13a)$$

From equation 14,

$$t_a=\frac{0.4t_p\,400+0.7t_g\,113.6+0.7t_w\,286.4+1.1t_f\,400+0.7t_i\,400+0.24\times297\times0}{0.4\times400+0.7\times113.6+0.7\times286.4+1.1\times400+0.7\times400+0.24\times297}$$

$$t_a=0,13t_p+0.0645t_g+0.163t_w+0.357t_f+0.227t_i \qquad (14a)$$

From equation 15,

$$t_f=\frac{1.08(0.3t_i+0.41t_p+0.086t_g+0.234t_w)+1.1t_a+0}{1.08+1.10+0.283}$$

$$t_f=0.132t_i+0.0378t_g+0.103t_w+0.447t_a+0.18t_p \qquad (15a)$$

From equation 16,

$$t_w=\frac{1.08(0.326t_f+0.228t_i+0.290t_p+0.032t_g)+0.7t_a+0}{1.08(1-0.108)+0.70+0.291}$$

$$t_w=0.180t_f+0.126t_i+0.0176t_g+0.357t_a+0.16t_p \qquad (16a)$$

From equation 17,

$$t_g=\frac{1.08(0.08t_w+0.304t_f+0.253t_i+0.325t_p)+0.7t_a+0}{1.08(1-0.043)+0.7+3.09}$$

$$t_g=0.0179t_w+0.068t_f+0.0566t_i+0.145t_a+0.0725t_p \qquad (17a)$$

From equation 18,

$$t_i=\frac{1.08(0.072t_g+0.178t_w+0.300t_f+0.3t_p)+0.7t_a+0}{1.08(1-0.150)+0.7+0}$$

$$t_i=0.048t_g+0.1186t_w+0.200t_f+0.432t_a+0.2t_p \qquad (18a)$$

The six simultaneous equations are now in a form suitable for solution. If $t_f t$— is eliminated:
From equations 15*a* and 16*a*,

$$0.132t_i+0.0378t_g+0.103t_w+0.447t_a+0.18t_p=5.56t_w-0.700t_i-0.0977t_g-1.98t_a-0.89t_p$$
$$0.832t_i+0.1355t_g-5.46t_w+2.43t_a+1.07t_p=0$$
$$t_i=-0.1628t_g+6.56t_w-2.92t_a-1.29t_p \qquad (A)$$

From equations 16*a* and 17*a*,

$$5.56t_w-0.700t_i-0.0977t_g-1.98t_a-0.89t_p=14.7t_g-0.263t_w-0.832t_i-2.13t_a-1.068t_p$$
$$5.82t_w+0.132t_i-14.71t_g+0.15t_a+0.178t_p=0$$
$$t_i=111.5t_g-44.1t_w-1.138t_a-1.35t_p \qquad (B)$$

From equations 17*a* and 18*a*,

$$14.7t_g-0.263t_w-0.832t_i-2.13t_a-1.068t_p=5.00t_i-0.240t_g-0.593t_w-2.16t_a-t_p$$
$$14.94t_g+0.330t_w-5.83t_i+0.03t_a-0.068t_p=0$$
$$t_i=2.56t_g+0.0567t_w+0.00515t_a-0.0116t_p \qquad (C)$$

From equations 18*a* and 14*a*,

$$5.00t_i-0.240t_g-0.593t_w-2.16t_a-t_p=2.80t_a-0.181t_g-0.457t_w-0.636t_i-0.364t_p$$
$$5.64t_i-0.059t_g-0.136t_w-4.96t_a-0.636t_p=0$$
$$t_i=0.01047t_g+0.0241t_w+0.880t_a+0.1125t_p \qquad (D)$$

From equations 14*a* and 13*a*,

$$2.80t_a-0.181t_g-0.457t_w-0.636t_i-0.364t_p=560-4t_a-t_i-t_p-0.284t_g-0.716t_w$$
$$6.8t_a+0.103t_g+0.259t_w+0.364t_i+0.636t_p-560=0$$
$$t_i=1{,}538-0.283t_g-0.715t_w-18.7t_a-1.75t_p \qquad (E)$$

From the five equations, *A*, *B*, *C*, *D*, *E*, the temperature of inside-wall surface is now eliminated:
From equations *A* and *B*,

$$111.7t_g-50.7t_w+1.78t_a-0.06t_p=0$$
$$t_g=0.455t_w-0.016t_a+0.00005t_p \qquad (F)$$

From equations *B* and *C*,

$$108.9t_g-44.2t_w-1.143t_a-1.34t_p=0$$
$$t_g=0.406t_w+0.0105t_a+0.0123t_p \qquad (G)$$

From equations *C* and *D*,

$$2.55t_g+0.0326t_w-0.875t_a-0.1241t_p=0$$
$$t_g=-0.0128t_w+0.343t_a+0.0487t_p \qquad (H)$$

From equations *D* and *E*,

$$0.2935t_g+0.738t_w+19.58t_a+1.86t_p-1{,}538$$
$$t_g=5{,}240-2.52t_w-66.9t_a-6.35t_p \qquad (I)$$

The glass temperature, t_g, can now be eliminated from the four equations; *F*, *G*, *H*, *I*:
From equations *F* and *G*,

$$0.049t_w-0.027t_a-0.0118t_p=0$$
$$t_w=0.551t_a+0.241t_p \qquad (J)$$

From equations *G* and *H*,

$$0.407t_w-0.332t_a-0.0364t_p\doteq0$$
$$t_w=0.816t_a+0.09t_p \qquad (K)$$

From equations *H* and *I*,

$$2.507t_w+67.243t_a+6.399t_p-5{,}240$$
$$t_w=2{,}090.1-26.82t_a-2.55t_p \qquad (L)$$

The wall temperature can now be eliminated from equations *J*, *K*, and *L*:
From equations *J* and *K*,

$$0.265t_a-0.151t_p=0 \text{ or } t_a=0.571t_p \qquad (M)$$

From equations K and L,

$$2{,}090.1-27.636t_a-2.64t_p=0 \text{ or}$$
$$t_a=75.62-0.0956t_p \qquad \text{(N)}$$

If we eliminate t_a and solve equations M and N for the equilibrium panel temperature,

$$0.6666t_p=75.62 \text{ and } t_p=113.44 \text{ degrees Fahrenheit}$$

Then from equation M,

$$t_a=0.571\times113.44=64.77 \text{ degrees Fahrenheit}$$

From K,

$$t_w=0.816\times64.77+0.09\times113.44=63.06 \text{ degrees Fahrenheit}$$

From F,

$$t_g=0.455\times63.06-0.016\times64.77+0.00005\times113.44=27.6 \text{ degrees Fahrenheit}$$

From A,

$$t_i=-0.162\times27.6+6.56\times63.06-2.92\times64.77-1.29\times113.44=72.73 \text{ degrees Fahrenheit}$$

From 18a,

$$t_f=5.00\times72.73-0.240\times27.6-0.593\times63.06-2.16\times64.77-113.44=66.30 \text{ degrees Fahrenheit}$$

$$\text{Mean radiant temperature}=0.25\times66.3+0.25\times72.73+0.25\times113.44+0.071\times27.6+0.179\times63.06=76.36 \text{ degrees Fahrenheit}$$

Depression D, of air temperature below mean radiant temperature

$$D=t_a-\text{mean radian temperature}$$
$$=76.36-64.77=11.59 \text{ degrees Fahrenheit}$$

Effective comfort equivalent temperature within the room,

$$t_e=\frac{76.36+64.77}{2}=70.6 \text{ degrees Fahrenheit} \qquad (19)$$

(The foregoing numerical example has been carried out to $^1/_{100}$ of a degree even though the design constants and the empirical comfort equation do not justify such accuracy; the reason for so extending the work is to permit exact solution of the simultaneous equations.)

Determination of Necessary Energy Input. Sufficient heat must be supplied to the room to offset the conduction losses through exterior surfaces and the heat gain of the ventilation air. Since all equilibrium temperatures are now known, the required heat supply can be calculated readily from the equation:

$$Q_R=0.24W(t_a-t_o)+C_wA_w(t_w-t_o)+C_gA_g(t_g-t_o)+C_fA_f(t_f-t_o) \qquad (20)$$

The heat loss from the rear of the panel will not be included as it will be assumed either that there is a heated space on the floor above or that the panel is backed with sufficient insulation to reduce the heat loss to a negligible amount. If we substitute into equation 20,

$$Q_R=0.24\times297(64.77-0)+0.291\times286.4\times63.06+3.09\times113.6\times27.6+0.283\times400\times66.3=4{,}620+5{,}260+9{,}750+7{,}530=27{,}160 \text{ Btu per hour}$$

Comparison With Energy Input for Convective Heating. The panel-heating system designed in the previous sections would provide comfort in a room with air temperature of 64.8 degrees comparable to that which would be realized in a convection-heated inside room for which both air *and surfaces* are at 70.6 degrees Fahrenheit. For the room used in the example, or for any room having transmission losses, the surface temperatures would be lower than the air temperature if convective heating were used. Thus, to compare the energy requirements of the two systems on the basis of *equal comfort*, the relationship between mean radiant temperature and t_a must be established for the room with convective heating, and the air temperature to be maintained in this room then must be adjusted to satisfy the equation,

$$\frac{t_a+\text{mean radiant temperature}}{2}=70.6 \qquad (21)$$

In this equation, mean radiant temperature can be expressed as a function of t_a by noting that for any section of wall, s, the surface temperature can be calculated from the resistance proportionality,

$$\frac{t_s-t_o}{t_a-t_o}=\frac{1/C_s}{1/U_s}=\frac{U_s}{C_s}$$

or

$$t_s=t_o+\frac{U_s}{C_s}(t_a-t_o)$$

and when $t_0=0$,

$$t_s=\frac{U_st_a}{C_s} \qquad (22)$$

Then if we use equation 22 to express all surface temperatures and substitute the resultant expression for mean radiant temperature into equation 21,

$$\frac{t_a+t_a\left[\left(\frac{A_wU_w}{C_w}+\frac{A_gU_g}{C_g}+\frac{A_fU_f}{C_f}+A_i+A_c\right)\Big/(A_w+A_g+A_f+A_i+A_c)\right]}{2}=70.6 \qquad (23)$$

where A_c is equal to the ceiling area (assumed, for comparison with panel room, to have negligible heat loss). Now if we let $A_w+A_g+A_f+A_i+A_c=A_T$,

$$t_a\left(\frac{A_wU_w}{C_w}+\frac{A_gU_g}{C_g}+\frac{A_fU_f}{C_f}+A_i+A_c+A_T\right)=141.2A_T \qquad \text{(23a)}$$

If we substitute known terms into equation 23a,

$$t_a\left(\frac{286.4\times0.25}{0.291}+\frac{113.6\times1.13}{3.09}+\frac{400\times0.25}{0.283}+400+400+1{,}600\right)=141.2\times1{,}600$$

giving $t_a=74.3$ degrees Fahrenheit.

For comfort equal to that of the panel-heated room, the air temperature in the convective-heated room therefore must be 74.3 degrees Fahrenheit, almost ten degrees Fahrenheit greater than that needed with radiant heating. The load for convective heating can then be calculated,

$$Q_c=(0.24W+U_wA_w+U_gA_g+U_fA_f)(t_a-t_o)$$
$$=(0.24\times297+0.25\times286.4+1.13\times113.6+0.25\times400)(74.3-0)$$
$$=74.3(71.5+71.9+128.5+100)$$
$$=27{,}600 \text{ Btu per hour} \qquad (24)$$

Panel Performance Characteristics

The room considered in the example has practically the same heat loss whether heated by a panel or by a convection system; the saving due to use of radiant heating, 1.6 per cent is negligible. Care should be exercised, however, in attempting to generalize from the result of this one numerical example as the difference in energy requirements between the two types of system differ widely with the architectural characteristics, ventilation rate, and outside design temperature of the installation. Frequently panel heating affords a definite and important reduction in energy requirements while in some cases there may be a slight loss. Because of this lack of consistent relationship between radiant- and convective-heat loads, the most satisfactory procedure in designing a radiant installation is to use the rational heat-balance procedure even though it is tedious and time-consuming.

A point of particular interest with respect to comparative characteristics of heating systems is that a given load comparison is valid only for the specific design conditions used in the computation. If we refer again to the 1.6 per cent saving due to panel heating of the room used in the example, this figure is true only for an outside-air temperature of zero degrees. As the outside temperature varies, the respective loads of the two systems will not vary proportionally. Thus, in evaluating the true operating advantage of one type of system over the other, the energy requirements should be determined for a range of outside temperatures and the seasonal power requirement obtained by summing the energy needs for all periods of operation. This condition represents a distinct variation in the performance of radiant systems and, if not given consideration, easily may be responsible for an incorrect conclusion as to the relative operating costs.

Aside from the problem of calculating the energy requirements of a radiant system, the heat-balance analysis is the *only* method by which the relationship between panel temperature and panel area can be evaluated. If we assume that the load on a given system is approximately determined by some other method and the maximum panel temperature is fixed by some structural limitation (as limiting temperature of a plaster ceiling), determination of the minimum panel area would still require establishing a heat balance on the system, as otherwise the rate of heat input to the room from one square foot of panel could not be established. This rate depends not merely on the room air temperature, but also on the temperatures and relative positions of the surrounding surfaces. For the room used in the example,

$$Q_p = \frac{Q_R}{A_p(t_p - t_s)} = \frac{27{,}160}{400(113.4 - 70.6)} = 1.58$$

Btu per (hour)(square feet)(degrees Fahrenheit) (25)

Based on 70 degrees Fahrenheit as the most commonly used equivalent temperature and 120 degrees Fahrenheit as a maximum panel temperature, the hourly maximum energy dissipation rate would appear to be of the order of 80 Btu per (hour)(square feet). This figure may vary widely with the conditions of each problem, and the value shown can be used safely only as rough over-all check.

Although intended as a radiant-heat source, the average panel dissipates a substantial fraction of its total energy output by convection. Again if consider the room used in numeral example:

Panel energy loss by convection

$$= h_p A_p (t_p - t_a)$$

$$= 0.4 \times 400(113.4 - 64.8) = 7{,}776 \text{ Btu per hour}$$

$$= \left(\frac{7.776}{27.160} \times 100\right) \text{per cent} = 28.5 \text{ per cent}$$

Thus a panel does not provide 100 per cent radiant heating, but it does transfer a larger fraction of energy by radiation than do most other heating systems. If surfacing materials having very high reflectivities were commercially available and aesthetically acceptable, comfort could be maintained at a very low air temperature and with an energy input insignificantly small in comparison with that required at present. The probability of realizing such a high degree of operating perfection is, from a practical point of view, remote, but the possibility does serve to indicate that a much more effective approach to true radiant heating than has so far been realized may yet be attained.

An acceptable criterion of panel performance, indicative of the extent to which the actual system approaches true radiant heating, is the depression, D, of room air temperature below the mean radiant temperature. A study of the fundamental equations will show that D increases as the ventilation rate increases and as the thermal resistance and outside-air temperature decrease. Panel temperature has only a small effect on D, the depression increasing slightly with increase of panel temperature. Thus the effectiveness of panel heating, as judged by D, is greater in cold climates and for poorly insulated structures having high ventilation rates. This does not mean, of course, that insulation is undesirable; rather it indicates that the saving due to use of panels in general will be greater in structures which have a high heat loss.

Selection of a maximum design panel temperature is dictated by considerations of uniformity of heating as well as by possible structural limitations. A small high-temperature panel may dissipate an adequate amount of heat and yet fail to provide occupancy comfort, because of the varying amount of irradiation experienced by the occupant in moving around the room. This problem requires a knowledge of the shape-factor variation of the human body with respect to a moving point on the ceiling; experimental data on such factors and analyses of typical distribution problems are available in the literature. As a first approximation, frequently inaccurate, the panel surface can be distributed as a border around the periphery of the ceiling. Such a pattern will not always provide adequate uniformity of irradiation, but, lacking the time or data required for a determination of the optimum pattern, it will assure more satisfactory results than would be attained if the entire panel surface were placed in one centrally located unit.

Summary

Radiant heating by means of low-temperature ceiling panels permits establishing comfort in a room with lower air temperature—and therefore a greater feeling of freshness—than is possible with heating systems of conventional type. Because of the reduced air temperature, the energy loss due to heating of ventilation air is less than usual and, in some cases, the over-all heat requirement for the system may be substantially less than that of a corresponding convection system.

Architecturally, panel heating is advantageous, because there need be no visible evidence of the heating element. When electrically energized panels are used, this advantage is increased, as neither stack, boiler, nor fuel storage is required, and the only visible evidence of a heating plant is the electric switch. Electric systems of many types have been used extensively, but need exists for a panel of this type which will combine good diffusion characteristics (to give a uniform surface temperature) with minimum thermal capacity (to assure rapid response to load changes).

The three steps which must be followed in designing a panel-heating system have been enumerated and discussed, and a detailed rational procedure has been presented for carrying out the analysis leading to determination of the required panel size (or operating temperature) and the energy requirement of the system. Limitations of the analysis result from failure to take into account interreflections and from possible inaccuracy in the physical constants used; the reflection limitation probably is not serious, as consideration of only the direct-radiation component should lead to a conservative result.

The panel system is of particular interest to the electrical engineer, because it seems to afford an unusually effective means of improving the competitive position of electricity as a primary energy source for space heating. Aside from direct electric panels, the lower operating temperature of panels using *any* heating medium makes off-peak electrical thermal storage more practical—because of smaller equipment—than it is with conventional heating systems; over 250,000 kw of such systems are now in service. Of even greater promise is the possibility of utilizing low-temperature panels with a heat pump for all-year conditioning, the lower winter surface temperature and higher summer surface temperature leading to an unusually high coefficient of performance. If an all-year heat-pump panel installation were combined with thermal storage, the potential market for off peak all-year electric energy would be great; such a potentiality is of as much interest to the utility—as a means of improving load factor—as it is to the consumer.

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A Resonant-Cavity Method for Measuring Dielectric Properties at Ultrahigh Frequencies

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Synopsis: A re-entrant cylindrical cavity has been adapted to measure the dielectric constant and power factor of small disk samples of insulating materials. The methods of measurement, mechanical details, and electric coupling and detecting circuits all are described. A single cavity can be utilized only over a frequency range of about 1.5 ratio from lowest to highest frequency. Therefore, several different-sized cavities would be required to cover a range of frequency from 50 to 1,000 megacycles. An accuracy of ±0.00005 in tan δ and ±1 per cent in dielectric constant may be obtained in routine measurements. Because the cavity has a very high $Q(>2{,}000)$, it is much more sensitive to low-power-factor dielectric samples than any conventional coil-and-capacitor resonant circuit.

The chief advantages of this method are that the operation of the apparatus is simple, very rapid, and similar to the susceptance variation technique now used at lower radio frequencies. Also, the involved computations usually found in other methods operative in this frequency range are eliminated.

THE frequency range of 50 to 1,000 megacycles always has proved a difficult range in which to measure the dielectric constant and loss of solid materials, because of residual lead and electrode inductance, resistance, and capacitance, and the difficulty of getting a solid sample to conform to the shape of the measuring device. Transmission-line methods of measuring a lumped impedance which have been treated recently by Hemple, Kaufman, Laville, Nergaard, Miller and Salzberg, and Chipman,[1] of course can be used to measure a dielectric sample considered as an impedance, but the problem remains here of attaching leads and electrodes to the dielectric so that they will not enter into the measurements, or of correcting for them if they do. Coaxial-transmission-line methods, where a section of (usually resonant) coaxial line is filled with the dielectric, are rigorous and satisfactory, but they require well-machined annular samples of considerable length, especially at the lower frequencies. Another method especially adaptable to high-loss materials is that described by Wyman,[4] where the phase angle and capacitance of a dielectric sample in this frequency range are compared to that of a standard sample with a cathode-ray tube. Liquids can be measured conveniently by filling a coaxial line, and liquids surrounding a parallel transmission line have been measured by Drude.[3] Liquids also can be measured readily in small coil and capacitor resonators which are totally immersed.[3]

At somewhat lower frequencies, below 100 megacycles and down to several hundred kilocycles the susceptance-variation technique of Hartshorn and Ward[6] has the advantage of practically eliminating the effect of series lead and electrode inductance and resistance. This method using a coil and capacitor arrangement has been applied up to 100 megacycles. At this frequency the dimensions of the usual type of coil and capacitor needed to obtain resonance start to become unusually small. The usual circuits, therefore, must be modified. The coils used also have relatively low Q values, so that insertion of low-loss dielectrics into the capacitor lowers the Q values of the circuit only slightly. The sensitivity of the apparatus therefore is relatively poor, when measuring low-loss materials. The resonant-cavity method described in this paper obviates most of these difficulties and has many advantages over the older methods.

Theory of Measurements

The transition of the coil and parallel-plate-capacitor resonant circuit at higher frequencies to a capacitor with a series of loops in parallel around it and from thence to a doubly (or singly) re-entrant closed cylinder is logical. This re-entrant cylinder is now a closed cavity capable of being resonated. It also may be thought of as a short-circuited quarter-wave-length transmission line with a capacitor at the open end. The capacitor actually makes the physical length of the line less than one-quarter wave length. Cross sections of a singly re-entrant cavity and a doubly re-entrant cavity are shown in Figure 1.

The dimensions of such cylinders, resonant in the frequency range 50–1,000 megacycles, are quite reasonable, their lengths varying from about two feet to two inches. This length is governed partly by the average capacitance at the end, and partly by the ratio of the radii of the inner and outer conductors.

CAPACITANCE

The dielectric under consideration is assumed to extend flush to the edges of the electrode posts and fill completely the space between the electrodes. This is not necessary, for the dielectric sample may be smaller than the area of the posts and may even have an air gap in series with it. In such cases standard equations for a combination of series and parallel dielectrics (air and sample) may be used to obtain the values for the sample, after the over-all dielectric properties of the space between the electrodes have been determined.

Let only that part of the apparatus which holds the dielectric be considered. The capacitance between the ends of the re-entrant cylindrical posts when the dielectric is in place is

C_x+C_{e1} [illegible]

These and all following symbols are defined in the appendix.

When the dielectric is removed, and the gap between the electrodes (ends of the re-entrant posts) has been closed sufficiently so that the cavity is resonant again at the same frequency, we have capacitance between the electrodes equivalent to what we had before. This capacitance is given by the expression

$$C_{a2}+C_{e2}-\frac{dC}{dS}\Delta S$$

where $(dC/dS)\,\Delta S$ is the correction term for the small change in length of the coaxial line which necessarily takes place when one of the posts is extended inward to diminish the gap.

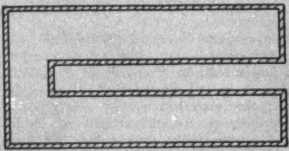

Figure 1A. Single re-entrant cavity

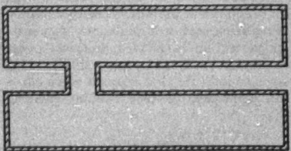

Figure 1B. Double re-entrant cavity

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Now, since the circuit is resonant in each case, the following relation is true:

$$C_x+C_{e1}=C_{a2}+C_{e2}-\frac{dC}{dS}\Delta S \qquad (1)$$

The capacitance of the electrode arrangement of two parallel-cylinder faces is determined by calibration using an audio-frequency bridge. This calibration is carried out so that the difference between the total interelectrode capacitance (including the fringing capacitance) at any two spacings of the gap is determined accurately. Thus from this calibration we know:

$$(C_{a2}+C_{e2})-(C_{a1}+C_{e1})$$

Then we have, using equation 1:

$$C_x=(C_{a2}+C_{e2})-(C_{a1}+C_{e1})+C_{a1}-\frac{dC}{dS}\Delta S \qquad (2)$$

With the exception of the last term, this is the same expression as is used in the susceptance-variation method to obtain the capacitance of an unknown. It is subject to an approximation, which is valid only if the change in S, ΔS, to retune the cavity to resonance after the sample is removed, is small compared to S. We also have made the assumption that C_{e1}, the fringing capacitance with the sample at spacing 1, is the same as without the sample for the same spacing. The correction term for the change in length, $(dC/dS)\,\Delta S$ is usually less than five per cent of C_x, so that a small error in $(dC/dS)\,\Delta S$ has a second-order effect on the value of C_x.

This correction term is derived as follows: The resonant frequency of a short-circuited transmission line with a capacitor at one end is given by the equation:

$$Z_0 \tan\frac{2\pi fS}{c}=\frac{1}{2\pi fC} \qquad (3)$$

The frequency characteristic of a re-entrant resonant cavity for the general case where the radius is appreciable, compared to a quarter wave length has been treated in some detail by Hansen.[4] However, if the radius of the resonant cavity is not very large compared to a quarter wave length, its resonant frequency may be obtained to a good approximation by considering it a short-circuited transmission line with a capacitor at one end. The dimensions of the cavities were so chosen that equation 3 is very nearly satisfied. In particular, care must be exercised in choosing the radius of the cavity so that it will not be a large fraction of the wave length. If this is not done, the correction term will be less accurate than desired.

By differentiating equation 3 with respect to the gap distance S, the following expression for the correction term is obtained:

$$\frac{dC}{dS}\Delta S=-\frac{(2\pi f)^2C_2^2}{c}\,188\log_{10}\frac{b}{a}\times\left(\sec^2\frac{2\pi fS}{c}\right)\Delta S \qquad (4)$$

It has been found practical to plot graphs of this function versus ΔS for various values of f and C_2, from which graphs interpolation can be made to obtain the correction term at any particular measurement. Such a series of curves is shown in Figure 7.

The assumptions which have been made in this method of measurement are justified experimentally in the results which have been obtained for the dielectric constant. Measurements made on samples of various thicknesses of the same material agree well, and this would not be the case if these assumptions were not valid. Measurements made on materials like polystyrene and quartz, which have very little change of dielectric constant over a wide range of frequency, agree well with lower- and higher-frequency measurements by independent methods and those of other experimenters.

Dissipation Factor

It remains now to determine the dissipation factor tan δ_x of the dielectric sample. In a resonant circuit, the following relation holds:

$$\text{Tan}\ \delta_{\text{total}}=\tan\delta_e+\tan\delta_D \qquad (5)$$

where tan δ_e is the dissipation factor of the empty cavity. Included in tan δ_e are all the effects due to coupling elements, measuring elements, and the ohmic resistance of the inside surface of the cavity. Tan δ_D would be the dissipation factor of the sample being measured if the sample filled the whole cavity, but, since the sample does not fill the whole cavity, tan δ_D is less than the dissipation factor of the sample by a factor (C_x/C_T). Then

$$\text{Tan}\ \delta_D=\tan\delta_x\frac{C_x}{C_T}$$

From equation 5 and the relation between tan δ_D and tan δ_x the following expression is obtained:

$$\text{Tan}\ \delta_x=\frac{C_T}{C_x}\tan\delta_{\text{total}}-\frac{C_T}{C_x}\tan\delta_e \qquad (6)$$

Equation 6 may also be written in its equivalent Q form:

$$\frac{1}{Q_x}=\frac{C_T}{C_x}\frac{1}{Q_T}-\frac{C_T}{C_x}\frac{1}{Q_e} \qquad (6a)$$

Now

$$\frac{1}{Q_e}=\frac{\Delta C_2}{2C_T},\ \text{and}\ \frac{1}{Q_T}=\frac{\Delta C_1}{2C_T} \qquad (6b)$$

whence

$$\tan\delta_x=\frac{\Delta C_1}{2C_x}-\frac{\Delta C_2}{2C_x} \qquad (6c)$$

Therefore, it is not necessary to know the total equivalent capacitance of our circuit (cavity) if the Q values are measured by changing the capacitance in our cavity by a known amount. ΔC_1 and ΔC_2 are, respectively, the variation in capacitance necessary to detune from one half-power level of resonance to the other, when the cavity is empty (ΔC_2), and when the cavity has dielectric in it (ΔC_1).

In a conventional coil and capacitor circuit it is not difficult to detune from resonance to half power by changing the capacitance, for only an additional parallel vernier capacitor is necessary. In a re-entrant cavity it is more difficult, since the only variable capacitance is that between the re-entrant ends. This may be varied readily when there is no dielectric in place, but complications enter when there is a dielectric sample between the re-entrant ends. Therefore, an indirect measurement of the ΔC value must be made. This can be accomplished readily if a value of dC/df is known. Then the frequency, f, may be varied to detune the cavity to half power from maximum resonant voltage. From a Δf value, a corresponding ΔC value may be obtained from a calibration chart.

The dissipation factor of the empty cavity is generally but one tenth of the smallest value obtained with the usual circuit elements. This makes the device much more sensitive for measuring very low-loss dielectrics than the susceptance-variation circuit used by Hartshorn and Ward.[5] The theoretical Q of such a cavity will be of the order of magnitude of that of a resonant quarter-wave-length line of the same cross section. The ex-

Figure 2. Measuring apparatus for use at 200 megacycles

Left to right: galvanometer, measuring cavity, microammeter, cavity wavemeter, oscillator pression for the Q of a coaxial line of this sort is

$$Q=4\pi\sqrt{\sigma/10}\,b\,\frac{\log_e b/a}{b/a+1}\sqrt{f}\times 10^{-6} \qquad (7)$$

The optimum Q is obtained with a ratio of b/a equal to about 3.6. Because of certain design features in the actual cavity, such as a bellows forming part of the inner conductor, and because of energy absorbed by measuring probes and the effect of the coupling-in loop, the theoretical Q which can be as high as 10,000 never is reached. The authors usually obtained one quarter to one half of the theoretical Q for the empty cavity.

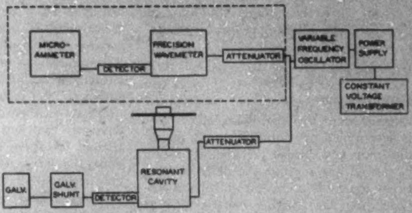
A single-resonant cavity of this sort can be operated over a frequency range of 1.0 to 1.5 times the lower frequency, using a practical value of capacitance in the space where the dielectric is placed. Several different-sized cavities thus are necessary to measure dielectrics over the entire frequency range 50 to 1,000 megacycles.

Description of Apparatus

The apparatus in Figure 2, which operates in the frequency range of 150 to 300 megacycles, has been used by the authors for some time. Another apparatus exactly like this one except that the cavity and wavemeter are scaled down in size is in use in the frequency range of 400–600 megacycles. Equipment to operate at higher and lower frequencies is planned. This equipment is convenient to use and requires little space. An advantage of this method over the coaxial-line method used by other experimenters is the small disk-shaped sample required and the ease of preparing the sample for measurement. The sample required is $1^{1}/_{2}$ inches or less in diameter and from 0.050 to one-quarter inch thick. Such a piece of material may be readily molded, cast, or machined, in the laboratory. The simple shape of the sample is of particular advantage in the case of ceramics.

Figure 3 is a schematic-circuit diagram of the setup shown in Figure 2. The power supply which is mounted elsewhere is not shown in the picture. It is energized from a constant-voltage transformer. The power supply is a conventional voltage-regulated supply delivering a maximum plate current of 80 milliamperes at 450 volts and a filament current of 3.65 amperes alternating current at two volts. The oscillator shown is a commercial model made by a well-known radio manufacturer. Its frequency is continuously variable from 150 to 600 megacycles. We also have constructed another oscillator which proved satisfactory. The attenuators shown in the circuit of Figure 3 are required so that any change taking place in either the cavity or wavemeter will not affect the frequency of the oscillator. From five to ten decibels has proved satisfactory, and, since the frequency of the oscillator must be varied in making the measurement, attenuators which are frequency insensitive are required. Because it is difficult to design and construct attenuators which are frequency insensitive at this frequency, it was decided to use long lengths of high-loss cable. Such long cables are frequency insensitive since they always present their characteristic impedance to the oscillator. Coils of this cable were hung on the back of the table and therefore did not show in Figure 2. The re-entrant resonant cavity in which the material to be measured is placed is shown in Figures 4 and 5.

Figure 3. Arrangement of apparatus for measurement of dielectric properties by the wavemeter method



The exciting loop is made small and placed near the bottom where the current is a maximum. A small loop is used both to keep the Q high and to couple in only a small amount of energy. A strong field across the sample is not desired, because it is desired to avoid all heating of the sample. To determine the dielectric constant, the empty cavity must be tuned to the frequency to which the cavity with the sample between the electrodes was tuned. This is accomplished by varying the gap between the electrodes. Therefore, fine adjustmennt of this gap is required. This adjustment is obtained by means of the differential-screw assembly shown in Figure 6.

To insure a positive contact all slide contacts are eliminated and a Sylphon bellows is used to connect the movable electrode with the top of the cavity. The face of the moving electrode is held rigid and parallel to the fixed electrode by the differential-screw assembly (Figure 6).

The wavemeter is constructed exactly the same as the cavity of Figure 5, all dimensions being the same, except that the differential-screw assembly is shorter, since not so great a travel of the top electrode is required. Although any wavemeter capable of the required precision may be used, a wavemeter exactly the same as the cavity eliminates the need of a calibration curve. A slightly different method of coupling was used. A probe near the top of the cavity excites the cavity. This results in a higher Q than is obtained in the case of the other cavity. The Q of the wavemeter is about 4,000. This is desirable, because the wavemeter should tune as sharply as possible.

When the wavemeter is tuned to resonance, a very intense field is set up between the two electrodes forming the re-entrant portion of the cavity; therefore, this device is very sensitive to any change in the position of the electrodes. A movement of less than $^{1}/_{240{,}000}$ of an inch may be detected. In the case of power factors less than 0.0003 it is necessary to be able to read the position of the electrode to within $^{1}/_{240{,}000}$ of an inch to obtain an accuracy of five per cent. This means that a fine-thread screw and a vernier capable of reading a very small part of a turn must be used. Since all backlash should be removed from this screw, it is necessary to load it with a spring, and, since it is desirable to avoid using a very fine thread, a differential screw was used having screws with 20 threads per inch and 24 threads per inch, which is equivalent to a screw of 120 threads per inch. The vernier dial on this assembly reads $^{1}/_{2{,}000}$ of a turn which gives the position of the electrode to $^{1}/_{240{,}000}$ inch.

The detector units for the wavemeter and cavity consist of two crystals in parallel connected to a probe. These crystals act as a full-wave rectifier. The detector assembly was designed to keep all shunt capacitance very small, so that the detector frequency is insensitive over a wide range of frequencies. The radio-frequency current is by-passed by large capacitors built into the detector housing. The crystals operate on such a portion of their characteristic curve that the rectified current measured by an indicating instrument connected to the detector reads a current proportional to E^2 in the cavity. The length of the probe which projects into the cavity may be varied without disturbing the electric circuits of the detector. Other types of detectors such as a diode, bolometer, or thermocouple may be used. A diode requires a power supply and is less sensitive and less stable than a crystal. It also is more frequency-sensitive and not as compact. The sensitivity of a bolometer or thermocouple is not nearly so good as that of a crystal and their use requires a bridge circuit of either the balanced or unbalanced type.

The detector in the wavemeter is connected directly to a microammeter, while the detector in the cavity in which the sample is placed is connected to a sensitive galvanometer (Rubicon 3402 H.H.

sensitivity 0.005 microampere per millimeter) through a shunt. The galvanometer shunt is a ladder network so arranged that the crystal always sees the same impedance regardless of which shunt is in use and the critical damping resistance is always across the galvanometer.

Calibration of Apparatus

If the dissipation factor of the cavity without the sample is known for all positions of the electrode, the work of measuring is cut in half. Therefore, it is advisable to obtain a curve of dissipation factor of the cavity without sample versus position of the electrode. Since the method of obtaining the dissipation factor of the empty cavity is the same as that of obtaining the dissipation factor of the cavity with the sample in place, this will be treated under measurements. A further advantage is that, once the dissipation factor of the cavity without the sample is known, the entire measurement may be made at the resonant frequency by the "voltage method" (see equation 13).

The dissipation factor of the 200-megacycle cavity without a sample was found to be nearly constant for all separations of the electrodes. To obtain the dielectric constant we must apply two correction factors. For equation 2 we must obtain the value of $(C_{a2} + C_{e2}) - (C_{a1} + C_{e1})$ and $(dC/dS)\Delta S$. C_{a1} is calculated from the geometry of the electrodes. The value of $(C_{a2} + C_{e2}) - (C_{a1} + C_{e1})$ is obtained from the curve of capacitance versus separation of the electrodes as measured on an audio-frequency bridge. Because of stray capacitance in the circuit which could not be determined accurately, the absolute value of the capacitance between electrodes could not be measured, but the value of capacitance by difference was obtained for various separations of the electrodes, which is expressed by $(C_{a2} + C_{e2}) - (C_{a1} + C_{e1})$. The fringing correction $(C_{e2} - C_{e1})$ has been found to be about one percent or less with $1^1/_2$-inch electrodes used in the 200-megacycle cavity shown in Figure 5.

Figure 4. Interior view of the measuring cavity for use at 200 megacycles

This cavity is ten inches long and six inches in diameter

The value of $(dC/dS)\Delta S$ in equation 2 is obtained from a curve of ΔC versus ΔS, where $\Delta C = (dC/dS)\Delta S$, and ΔS is the change in S, the length of the inner conductor, when the sample is removed and the cavity is again tuned to resonance at the same frequency. Such a curve is calculated from equation 4 for each value of frequency. By choosing values of frequency at convenient intervals over the frequency band covered by the cavity, a family of curves is obtained as shown in Figure 7. Interpolating between curves, the value of ΔC corresponding to ΔS may be found for any frequency at which a measurement is made.

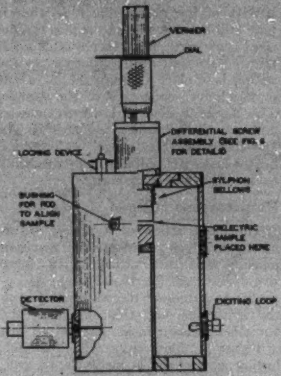

VERNIER
DIAL
DIFFERENTIAL SCREW ASSEMBLY (SEE FIG. 6 FOR DETAIL)
LOCKING DEVICE
SYLPHON BELLOWS
BUSHING FOR ROD TO ALIGN SAMPLE
DIELECTRIC SAMPLE PLACED HERE
DETECTOR
EXCITING LOOP

Figure 5. The re-entrant cavity for measuring dielectric properties of materials

The resonant frequency versus electrode separation of the empty 200-megacycle cavity is shown in Figure 8. Since the wavemeter that was used in these measurements is identical with the measuring cavity, Figure 8 also is the resonant-frequency - versus - electrode - separation characteristic of the wavemeter. The wavemeter provides the data from which $\tan \delta_x$ is calculated and also indicates the frequency at which the measurements are made. This curve was obtained by heterodyning the oscillator which excites the cavity with a signal generator or oscillator whose frequency is accurately known and measuring the beat frequency by a suitable device. If the wavemeter were not identical to the measuring cavity, then a curve of wavemeter reading versus electrode separation of the cavity when both were tuned simultaneously to the same frequency would be required.

The crystal response was checked and found to obey the square law for the crystal current magnitude used in the measuring equipment.

Methods of Measurement

Wavemeter Method

With the apparatus arranged as shown in Figure 3, the top of the cavity is removed and a disk of dielectric material is placed between the electrodes. The sample has the same diameter as the electrodes or less. It may be from $^1/_{32}$- to one-quarter-inch thick. A locking device assures that the top of the cavity always is replaced in the same position. The sample is centered between the electrodes by means of three rods which extend through equally spaced bushings in the side of the cavity. After the sample has been centered, the rods are withdrawn from the cavity. The top electrode then is brought into contact with the sample or, in the case of samples that are easily compressed, a known air gap is left in series with the sample. D_1 is designated as the separation of the electrodes, when the sample is in place as specified.

The frequency of the oscillator is varied until the resonant frequency (maximum response) of the cavity is found and the wavemeter is tuned to this frequency and its reading noted. Then, the frequency of the oscillator is varied until the response of the cavity is reduced to half; the wavemeter is tuned to this frequency and its reading noted. A corresponding measurement is made on the other side of the resonant frequency. The oscillator is set at the resonant frequency, and the sample is removed from the cavity. The cavity is retuned to resonance, and the electrode separation, D_2, is noted. Unless a curve of dissipation factor of the cavity versus separation of electrodes has been obtained previously, the procedure of detuning to half power previously given for the cavity with the sample in place must be repeated for the cavity with the sample removed.

In the case when the sample fills the entire space between electrodes, the dielectric constant

$$\epsilon' = C_x/C_a \tag{8}$$

where C_x is found from equation 2, and C_{a1} is the calculated air capacitance of the sample. A rough value of ϵ' may be obtained from the relation

$$\epsilon' = \frac{D_1}{D_2} \tag{9}$$

which is simply the ratio of the electrode separations with and without the sample. This approximate expression usually gives the dielectric constant to within five per cent but may be in error by as much as ten per cent.

The dissipation factor $\tan \delta_x$ is given by equation 6c. In equation 6c, we may place

$$\frac{\Delta C_1}{2C_x} \cong \frac{D_1' - D_1''}{2D_2} \tag{10}$$

and

$$\frac{\Delta C_2}{2C_x} \cong \frac{D_2' - D_2''}{2D_2} \tag{11}$$

Then from equations 6c, 10, and 11,

$$\tan \delta_x \cong \frac{D_1' - D_1''}{2D_2} - \frac{D_2' - D_2''}{2D_2} \tag{12}$$

Since only the electrode separations are required to solve this equation, it is a very convenient and rapid means of determining the dissipation factor of a sample.

The approximations for ΔC_1 and ΔC_2 in equations 10 and 11 introduce errors that usually are well within the precision with which D_1', D_1'', D_2', and D_2'' may be read, especially when measuring low-loss material. It should be noted that ΔC_1 and ΔC_2 are the differences of nearly equal quantities. However, when materials having high loss are being measured and ΔC_1 is large, the terms $C_{e1} - C_{e2}$ and $(dC/dS)\,\Delta S$ of equation 2 may be large. Then ΔC_1 should be found in the same way that C_2 is found from equation 2.

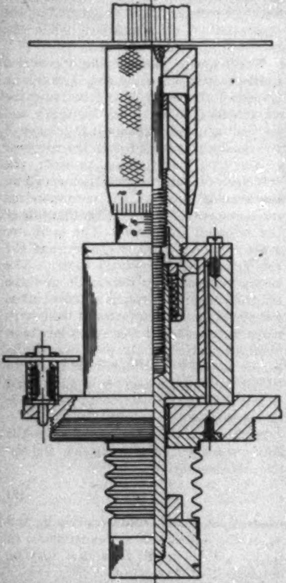


Figure 6. The differential-screw assembly for the re-entrant cavity

Since it is not possible to prepare all samples so that their opposite faces are absolutely parallel or the surfaces perfectly smooth, there is an air gap between the electrodes and sample when the electrodes are brought into contact with the sample. The effect of this unavoidable air gap may be minimized in either of two ways. If it is possible to determine the size of the sample accurately, an air gap of known dimension may be placed in series with it. This method has the serious disadvantage, especially in the case of low-loss materials, of reducing the sensitivity of the measurement, so that it may still be impossible to obtain the desired accuracy. The other way of minimizing the effect of irregularities in the sample is to place a thin metal foil of soft material over each face of the sample. This has been done at lower frequencies and was used in measurements in the 150–300-megacycle range. Correction for the losses in the foil is made according to the method of Hartshorn.[8]

HETERODYNING METHOD

The construction of a precise wavemeter involves considerable precision machine work, and is therefore an expensive operation. Therefore, it may be desirable to eliminate the wavemeter from the apparatus, and this eliminates the equipment shown inside the dotted line of Figure 3. This can be done if the following equipment is available: a calibrated oscillator or signal generator whose frequency or harmonics fall in the required band, a mixer (usually a crystal), and a calibrated receiver or other suitable detector. The frequency of this signal generator must be stable and its frequency known to the precision desired for the value of tan δ_x.

The frequencies at the half-power points (f_1', f_1'') and at resonance (f_1) of the measuring cavity with the sample in place are determined by beating the varied frequency of the oscillator feeding the measuring cavity against the fixed frequency of the second oscillator or signal generator. The two oscillator frequencies are fed through a crystal mixer into a calibrated receiver which measures their beat frequency or difference frequency. The capacitances, C_1' and C_1'' (between the electrodes in the cavity) necessary to produce resonance at f_1' and f_1'' then are found after the sample has been removed. The frequencies at the half-power points (f_2', f_2'') and at resonance (still f_1) with the sample removed are found by the same procedure. These correspond to capacitances C_2' and C_2''. Calculation of tan δ_x then proceeds according to equations 6c or 10–12.

There are several disadvantages to this method of measuring the frequency which make it more time-consuming than measurement by means of a wavemeter. If there is any instability in one of the elements, it is difficult to determine which one is unstable, making adjustment tedious. At higher frequencies when a signal generator is used, it becomes difficult to locate the beat frequencies, and, finally, a suitable receiver, if not already at hand, is an expensive instrument.

FREQUENCY-MODULATION METHOD OF DETERMINING THE DISSIPATION FACTOR OF A DIELECTRIC SAMPLE USING AN OSCILLOGRAPH

With the equipment arranged as in Figure 9, the saw-tooth wave from the sweep oscillator in the oscillograph is applied both to the horizontal deflecting plates and to a trigger circuit. The output of the trigger circuit, which is the derivative of a saw-tooth wave, is fed to a permanent-magnet loudspeaker, an integrating device. A speaker with the cone removed was mounted on the frame of a coaxial-line ultrahigh-frequency oscillator, similar in construction to one described by Barrow.[7] The movement of the speaker diaphragm rod varies the capacitance in the grid circuit of the oscillator and therefore varies the frequency by a small amount. The oscillator delivers a signal which is frequency-modulated in accordance with the saw-tooth sweep applied to the horizontal plates of the oscillograph. The modulated output of the oscillator is fed to a precision wavemeter and to the cavity containing the dielectric sample. The resonant cavity changes the frequency-modulated signal into an amplitude-modulated signal so that a resonance

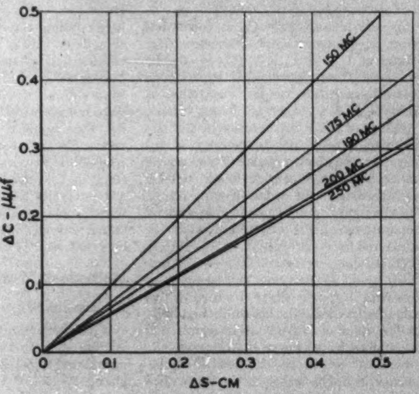


Figure 7. These curves show the change in capacitance versus change in separation of electrodes at constant frequency

200-megacycle cavity used in this research curve appears on the screen of the oscillograph.

When the precision wavemeter is tuned to the resonant frequency of the cavity, a pip appears on the resonance curve on the oscillograph screen because of the absorption of power by the wavemeter. By adjusting the gain controls of the vertical and horizontal amplifiers, the resonance curve of the cavity is made to coincide with the universal resonance curve drawn on the screen of the oscillograph with its half-power points marked with vertical lines. The band width of the resonance curve now can be measured by running the wavemeter pip along this curve from one vertical line to the other. By noting the dial reading of the wavemeter when the pip coincides with each vertical line in each case (that is, sample in place and removed) the same formulas given in a previous section of this paper may be used to obtain tan δ_x, the dissipation factor of the dielectric.

The chief difficulty encountered in using this method is that the band width of the cavity is so narrow and the wavemeter pip of such considerable width in comparison that it is impossible to get an accurate measurement of the band width. The same difficulty occurs to a lesser extent in the case of the cavity with the sample in place. However, it is possible to raise the Q of the wavemeter considerably and to lower the Q of the measuring cavity. This should overcome this difficulty. When this equipment is working properly, measurements may be made much faster than when using the first method described (varying the frequency manually to reduce the response to half on each side of resonance). However, it is not so fast nor convenient as the voltage method.

Another disadvantage is the fact that introducing a capacitance into the grid circuit of the oscillator lowers its frequency so that it must be designed for a much higher frequency when its signal is modulated in this way. Also, in the case of high-loss material, it is difficult to modulate enough to obtain a sufficient portion of the resonance curve to make measurements.

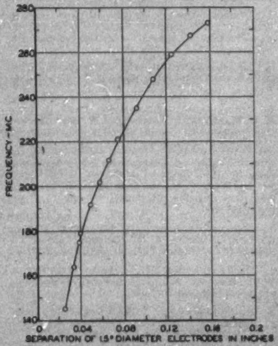


Figure 8. This curve shows the relation between resonant frequency and the separation of the electrodes of the cavity

200-megacycle cavity used in this research

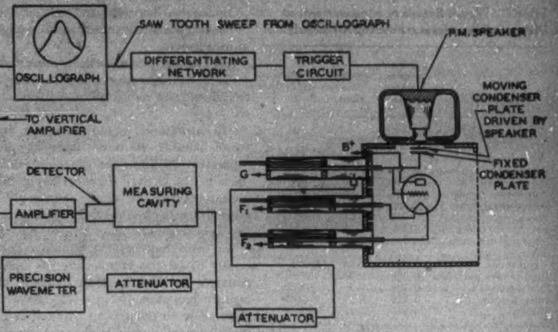


Figure 9. Arrangement of apparatus for measuring dielectric properties of materials by the frequency-modulation method

VOLTAGE METHOD

In this method the sample is placed in the cavity, the oscillator tuned to resonance, and the galvanometer reading noted. Then the sample is removed from the cavity, and the cavity tuned to resonance, the frequency remaining constant, and the galvanometer reading noted. The dielectric constant may be calculated in the usual manner, and the dissipation factor tan δ_x, is calculated from the equation:

$$\text{Tan } \delta_x = \frac{V_0 - V_T}{V_T} \frac{\Delta C_2}{2C_x} \qquad (13)$$

It should be recalled that $\Delta C_2/2C_x$ is the dissipation factor of the empty cavity and its value is obtained before any measurements are made on samples, as explained in the section on calibration of apparatus. When the expression $(V_0 - V_T)/V_T$ is less than unity, the absolute accuracy obtained is better than that of the other methods.

Since this method is much faster and more convenient than the others, it is preferred in most cases by the authors. The time required to make a measurement is practically the time required to place the sample in the cavity and then remove it, as the sample need remain only long enough for the galvanometer deflection to be read. If the apparatus is to be used in the routine testing of samples that have similar dielectric properties, a wavemeter is not needed. However, if the apparatus is to be used in the laboratory to test materials having a wide range of dielectric properties, the use of a wavemeter is recommended.

Representative Measurements of Dielectrics

The results of the measurement of a few typical dielectrics are given in Table I. The power factor of these materials varied from 4.7 to 0.01 per cent. These samples also varied considerably in thickness. The measurements of various thicknesses of sample A of polystyrene show that the measurements are independent of the thickness of the sample. Samples were desiccated previous to measurement.

Accuracy of Method

The accuracy obtainable in this re-entrant cavity method of measurement is about ±0.00005 in tan δ_x and about ±1 per cent with optimum-size sample, in dielectric constant. The sensitivity or accuracy of the re-entrant cavity as used in this method of measurement is limited largely by the precision and sensitivity of changing and measuring the change in separation and absolute separation of the electrodes.

The accuracy of the dissipation-factor measurement rests upon the assumption that extension of the bellows with a fixed frequency in the cavity does not change the Q of the cavity. This assumption was substantiated by test measurements. The movement of the bellows is never more than $^3/_{16}$ inch and usually less than one-eighth inch. Measurement of the Q of the empty cavity varies only slightly with frequency (obtained by changing the gap separation), almost within the precision of measurement, and within the predicted theoretical change.

The accuracy of all the dielectric-loss measurements described in this report depends upon the assumption that the universal resonance curve holds for our cavity as arranged with the coupling loop and feeding line. In other words the voltage

Table I. Typical Results of Measurements of Dielectric Properties at About 200 Megacycles

Material	Thickness of Sample (Inches)	Dielectric Constant	Dissipation Factor
Micarta number 254 Cresol-formaldehyde resin, paper filled	0.150	3.72	0.047
Columbia resin *CR*-39	0.149	2.96	0.027
Pure fused quartz	0.206	3.79	0.0001
Polystyrene sample *A*	0.150	2.55	0.0003
Polystyrene sample *A*	0.100	2.565	0.0003
Polystyrene sample *A*	0.250	2.56	0.0003
Polystyrene sample *B*	0.151	2.56	0.0005
Polyvinyl carbazole	0.115	3.06	0.0009
Special styrene copolymer	0.183	2.64	0.008
High-tension porcelain	0.186	5.90	0.010
Zircon porcelain	0.178	9.5	0.0608
Ultra steatite	0.215	5.23	0.0007
Steatite	0.247	5.45	0.0034

variations in the cavity are assumed to correspond to frequency changes of the oscillator source, according to the resonance curve of the cavity, and are independent of coupling elements. The response of the cavity has been checked with the universal resonance curve and close agreement obtained.

Summary

A new method for measuring the dielectric properties of insulating materials in the 100- to 1,000-megacycle range has been developed. The theory and several methods of operation are described. The theory on which this method is based has been justified experimentally. Greater sensitivity and accuracy have been obtained than is possible by other methods operative in this frequency range. This method offers the following advantages:

1. The samples required are of simple disk shape which are easily prepared.
2. Both the measurements and the calculations are simple, making possible rapid determinations of both dielectric constant and power factor.
3. These characteristics make the apparatus suitable not only for laboratory use but also for production control where a relatively large number of samples may be involved.

Appendix. Definition of Symbols

A—Area of electrodes in the cavity in square inches.

a—Outside radius of inner conductor of cavity in centimeters.

b—Inside radius of outer conductor of cavity in centimeters.

c—Velocity of light in centimeters per second.

C—Capacitance between the ends of the re-entrant posts (electrodes) in the cavity at resonance.

C_x—Capacitance of sample being measured.

C_T—Total equivalent capacitance of cavity considered as a resonant coil and capacitor.

C_{a1}—Air capacitance, excluding fringing, between parallel electrode faces at the separation existing when the sample is in place in the cavity. Unless there is an air gap between the sample and the electrodes, this is the calculated air capacitance of the sample.

C_a—Air capacitance of the sample. This equals C_{a1} if the sample has the same diameter as the electrodes, and is in contact with them.

C_{e1}—Fringing air capacitance with the electrode separation existing when the sample is in place in the cavity.

C_{a2}—Air capacitance, excluding fringing, between the parallel electrode faces at separation 2, when the sample has been removed and the cavity retuned to resonance by closing the electrode separation.

C_{e2}—Fringing capacitance at separation 2.

$C_1 = C_{a1} + C_{e1} =$ approximately C_{a1}

$C_2 = C_{a2} + C_{e2} =$ approximately C_{a2}

C_1', C_1''—Capacitances between the electrodes of the empty cavity, or ($\Delta C_1 = C_1' - C_1''$) of an identical wavemeter, at f_1' and f_1'', respectively.

C_2', C_2''—Capacitances between the electrodes of empty cavity, or of ($\Delta C_2 = C_2' - C_2''$) an identical wavemeter at f_2' and f_2'', respectively.

D_1—Separation of electrodes with the sample in the cavity.

D_2—Separation of electrodes when the sample has been removed and the cavity retuned to resonance.

D_1', D_1''—Separation of electrodes in the cavity or the wavemeter identical to the cavity when tuned to frequencies f_1', f_1''.

D_2', D_2''—Separation of electrodes of the cavity at the half-power points when the cavity is empty.

δ—Loss angle.

f—Frequency of oscillation, cycles per second.

$(Q_1 = Q_T)$—Q of cavity with dielectric inserted.

$(Q_2 = Q_0)$—Q of cavity when dielectric has been removed.

S—Length of internal conductor of coaxial part of cavity in centimeters.

ΔS—Change in S on retuning cavity to resonance after removing sample. This is the same as the change in separation of the electrodes.

σ—Conductivity of metal covering inside of cavity in mhos per centimeter (5.8×10^5 for copper).

ω—Angular frequency of oscillations.

Z_0—Characteristic impedance of coaxial part of cavity.

V_0—Voltage of resonance with sample removed from cavity.

V_T—Voltage of resonance with sample inserted.

r_0—Radius of electrode in centimeters.

r_s—Radius of sample in centimeters.

f_1', f_1''—Frequency at the half-power points with the sample in the cavity.

f_2', f_2''—Frequency at the half-power points with the cavity empty.

$\frac{1}{Q_x} = \tan \delta_x =$ dissipation factor of the dielectric sample $\cong \cos \theta$, the power factor

Tan δ_D—Dissipation factor of dielectric which fills the entire cavity.

Tan δ_0—Dissipation factor of the empty cavity.

ϵ'—Dielectric constant.

f_1—Resonant frequency of cavity with sample inserted.

E—Electric-field strength.

Capacitance is in farads in all equations.

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Conditions Controlling the Economic Selection of Prime Movers

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Synopsis: This paper attempts to clarify the effect of various controlling conditions on the magnitude of the evaluation constants used to select the optimum prime mover to be added to a system with a growing load. A hypothetical load has been set up which is assumed to grow at a known constant rate. Fixing on a policy of equal-sized units, two-unit installed reserve, and one-unit operating reserve regardless of load magnitude the progress of the individual generating units from the base to the top of the load curve can be readily studied. With the fundamental mechanics of the problem delineated the effects of unit availability, unit life span, rate of load growth, load cycle, load factor, unit capacity, and system reserve policy are analyzed.

From this analysis certain principles are apparent, among the most important being:

1. Historical data of past and present prime-mover experience are of very limited value.
2. Evaluation constants for future prime movers are not fixed in magnitude and must be redetermined from time to time for each individual system.

Having demonstrated the relative importance of the various controlling conditions this work is concluded with a recommended procedure for determining the evaluation constants for any practical case.

PROBLEMS of providing sources of energy supply may be divided into two broad classes:

1. Where the load in its chronological variations will remain at its initial level indefinitely into the future.
2. Where the general load level will continue to increase into the foreseeable future.

In the first class the annual prime-mover outputs remain essentially constant with the passage of time. In the second class experience has demonstrated that annual outputs of the individual prime movers increase during the early part of their lives and subsequently decrease.

The difference in the behavior of the annual production rate of the prime movers in the two classes has a direct bearing on the economic techniques employed in arriving at the optimum selection of equipment. This behavior is further modified by other controlling conditions. Those conditions common to both classes of problems are

1. Availability of prime movers.
2. Life span of individual units.
3. System load cycle and load factor.
4. Unit capacity and reserve policy.

An additional controlling condition applicable to the second class is

5. Rate of system load growth.

Problems of the first class have been dealt with at length in the engineering literature. The thesis of this paper is the problem of the second class and the effects of the controlling conditions enumerated upon the economic selection of the prime movers.

In the field of engineering economics there are probably as many ways of presenting the basic philosophy as there are authors on the subject. Since no particular author has been universally accepted as a standard authority the following brief review of evaluation theory is presented to avoid any ambiguity that may arise due to the multitude of interpretations that may be applied to a given term.

Economic Evaluation

CONSTANT ANNUAL COSTS

The fundamental problem in the field of central energy supply is to produce the required energy at minimum total annual cost. That is, the selling cost or revenue must be kept at the lowest possible value compatible with reliability and quality. The total cost or revenue may be defined as

$$R=M\left(i+\frac{i}{(1+i)^n-1}+j\right)+C+H \qquad (1)$$

where

R = revenue or total energy cost in dollars
M = total investment in energy supply equipment in dollars
i = total cost of money use expressed as an interest rate (decimal)
n = useful life of equipment in years
j = sum of tax and insurance rates (decimal)
C = total operating costs in dollars
H = general overhead costs in dollars

The fractional term in the brackets is the uniform annual depreciation charge rate. The bracketed term is commonly called the fixed charge rate, and letting this be represented by y, the equation is simplified to

$$R=My+C+H \qquad (2)$$

It is evident that y may be based on other methods of computing depreciation than the sinking-fund method assumed in equation 1.

Basically the revenue is determined by the individual costs so that the problem is reduced to finding the justifiable additional investment for one prime mover versus any other in order to arrive at maximum economy for the given installation. Assuming that H remains constant regardless of the machine selected, it is evident that two machines a and b are equally desirable, if

$$M_ay+C_a=M_by+C_b \qquad (3)$$

This reduces to

$$M_a-M_b=\frac{C_b-C_a}{y} \qquad (4)$$

from which the optimum installation can be determined by comparison with the actual investments required for each machine. If the actual difference in investments is greater than indicated by equation four machine b is the optimum and vice versa. This method of comparison will hold only where the annual operating costs C remain constant during the useful life of the machine.

VARIABLE ANNUAL COSTS

When the annual production rate and hence the annual operating costs C are expected to vary during the life of the machine they must be reduced to an equivalent constant annual cost C'. This is done by first finding the present worth P of the series of varying annual costs $C_1, C_2, C_3, \ldots, C_n$ as follows:

$$P=\frac{C_1}{1+i}+\frac{C_2}{(1+i)^2}+\frac{C_3}{(1+i)^3}+\ldots+\frac{C_n}{(1+i)^n} \qquad (5)$$

The equivalent uniform annual cost is then

$$C'=P\frac{i(1+i)^n}{(1+i)^n-1} \qquad (6)$$

and it follows that

$$M_a-M_b=\frac{C_b'-C_a'}{y} \qquad (7)$$

Equations 4 and 7 are fundamental forms for arriving at the economic prime mover for new systems of supply or for extensions in capacity of existing systems.

As a matter of convenience it is generally sufficiently precise to assume that the annual operating costs C are proportional to the annual number of units of production V where V may be pounds of steam, kilowatt-hours, gallons of water,

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and so forth. Where this assumption holds, the calculations may be facilitated by letting the ratio of the equivalent uniform annual quantities to the first year quantities be designated by r, that is,

$$r=\frac{C'}{C_1}=\frac{V'}{V_1} \tag{8}$$

Then

$$C'=rC_1 \tag{9}$$

where r may be determined by use of the physical quantities of production in equations 5 and 6 and only the first-year operating cost need be calculated. By substitution of equation 9 in equation 7,

$$M_a-M_b=\frac{r}{y}\left(C_{1b}-C_{1a}\right) \tag{10}$$

Let

$$F=\frac{r}{y} \tag{11}$$

where F is the justifiable investment factor referred to the first year's annual operating cost.
Finally

$$M_a-M_b=F(C_{1b}-C_{1a}) \tag{12}$$

REPLACEMENT OF OBSOLESCENT PLANT

A third type of economic problem is the determination of the justifiable investment to effect a known saving in operating costs. For purposes of simplification assume that the general overhead costs are unaffected by the replacement of part or all of the plant and furthermore that the revenue would also be unaffected. Then let,

C_a = annual operating cost of existing system, in dollars
C_c = annual operating cost of system with new equipment, in dollars
T = annual taxes and insurance on associated retirements, in dollars

The saving in annual costs effected by the new equipment is

$$S=C_a-C_c+T \tag{13}$$

This saving will be offset by the fixed charges on the new equipment. It follows that the maximum value of investment that may be justified will be such that

$$My=S \tag{14}$$

or the justifiable investment is

$$M=\frac{S}{y} \tag{15}$$

Usually the investment will not be undertaken unless its value is less than that determined by equation 15. Again this determination is only valid for the condition that the annual saving will be constant during the life of the new equipment. It is evident that if the annual production rate of the new equipment varies, the annual savings in operating cost will also vary and for the same assumption upon which equation 8 is written, it follows that:

$$r=\frac{S'}{S_1}=\frac{V'}{V_1} \tag{16}$$

Then equation 15 may be written,

$$M=FS_1 \tag{17}$$

The Capacity-Factor Characteristic

VARIATION IN PRIME-MOVER ANNUAL OUTPUT

An energy-supply system that is experiencing a steady growth in load will require successive additions of generating units to meet the increased peak demands. Any one unit on such a system does not have a uniform annual production with increasing age. During the first few years of life, while serving on the base of the load curve, the annual output will increase slightly and then subsequently decrease when superseded as a base-load unit by new and usually more efficient units added to meet the increase n maximum energy demand. This variation of annual output is most conveniently expressed by the capacity factor as a percentage, defined as follows:

$$\text{Capacity factor} = \frac{\text{annual output}}{8{,}760\times\text{unit capacity}\times 100} \tag{18}$$

where the numerator and denominator are measured in the same numerical units.

The variation of turbine-generator annual outputs or annual capacity factors with age was first pointed out by Peter Junkersfeld.[1] Various organizations at about this time were collecting data on this item which showed that practically all units exhibited this characteristic as a general trend but that the magnitude of the factors and their duration in age varied considerably from unit to unit.

In this paper the plot of the annual capacity factors of a unit versus its age will be referred to as the capacity-factor characteristic. The shape of the characteristic is an important consideration in the economic evaluation of proposed equipment and it is necessary that the causes of its behavior be thoroughly understood. However, it must be emphasized that historical data of the sort mentioned above cannot be used to evaluate the selection of a proposed unit. Every effort should be made to forecast the future conditions on the system to which the addition of the new unit is contemplated and from such data to deduce the future annual outputs or capacity factors of the new unit.

Capacity factor being a convenient measure of annual output it may readily be used in place of the physical production quantities or annual operating costs in equations 5 and 6 to evaluate the ratio r for use in equations 11 and 16 and subsequent justifiable investment factor F in equations 12 and 17.

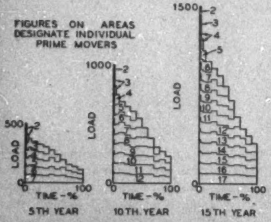

Figure 1. Annual load-duration curves showing individual-prime-mover outputs at 100 per cent availability

System peak load growing at a rate of 100 units per year with a constant load factor

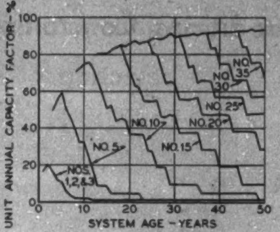

Figure 2. Variation of prime-mover annual capacity factor with system age

Unit life unlimited and availability at 100 per cent

EFFECT OF LOAD GROWTH

To study the effect of load growth on the capacity factors of the prime movers supplying that load the following assumptions were made. A system during its growth maintains a constant shape of annual-load-duration curve as defined in Table I.

A policy has been established that all units shall be of equal capacity, the installed reserve shall be equal to the capacity of two units, and the operating re-

Table I. Annual Load Duration

Per Cent of Peak Demand	Per Cent of Total Time
100	1
82	4
75	5
69	10
65	10
61	10
55	10
47	10
38	10
31	10
27	10
23	10

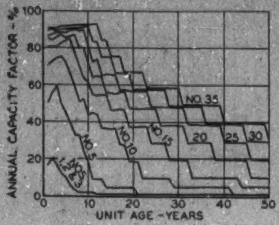

Figure 3. Variation of capacity-factor characteristic for successive units

Unit life unlimited and availability at 100 per cent

serve shall be maintained at a capacity of one unit. It is further assumed that the peak demand grows annually at an increment that is equal to the magnitude of the peak for the first year.

This means that three units will be installed the first year and one unit per year will be installed thereafter to maintain the installed reserve requirement. For the first three or four years of system life this high magnitude of installed reserve is probably somewhat unrealistic for most practical situations, whereas after an extensive growth of several years this magnitude may not be sufficient. However, by maintaining this condition constant for study purposes one variable is eliminated.

For simplification it is assumed that the load carried by the active units is divided proportionally among them. Since all units are of equal size the load is divided equally. In many practical cases it will be found that the proper division of load based on incremental costs is not much different from proportional loading. From an economy standpoint the effect of assuming this method of loading in this study is to underload the newer and presumably more efficient units and correspondingly overload the older and less efficient units. However, this effect is negligible compared to that of unit availability which is discussed later.

The position on the load curves and magnitude of output of the prime movers for the 5th, 10th, and 15th years of system life is illustrated in Figure 1. Note that the unit availability is taken at 100 per cent. The fifth, sixth, and seventh units which are the base load units in the fifth year are active only during the intermediate and peak loads in the tenth year. In the 15th year these units are rapidly approaching the classification of peak-load standby units. As the system grows, the number of base-load units increases; there are three in the 5th year, four in the 10th year, and five in the 15th year. Another important effect is the increased output of the base load units for the later years.

These points are brought out to better advantage by Figures 2 and 3. The annual capacity factors of every fifth unit and the first three units* are plotted against the system age in Figure 2. Each unit serves at the base of the load curve for the first few years of its life and then gradually proceeds to decrease its generation until it will be used only as a peak-load unit. Succeeding units serve the various levels of the load curve for longer periods than the preceding units. This is brought out by the capacity-factor characteristics plotted in Figure 3. The succeeding unit characteristics show a general increase in magnitude at a diminishing rate of increase and an extension in the time served at the various levels of capacity factor.

It will be readily apparent that the use of averaged historical data to determine the capacity-factor characteristic of a future unit on a given system is misleading. For example, if the experience of the first ten units were averaged to determine the capacity-factor characteristic of the 11th unit, insofar as that data would be available at the time the 11th unit would be required, the result would be radically different than that actually experienced by that unit.

Unit Availability

Prime movers become unavailable for two reasons:

1. For periodic inspection, maintenance, and overhaul.
2. Accidental breakdowns.

The former reason is ordinarily the major cause of unavailability. Obviously, the availability of a generating unit directly affects the magnitude of the capacity factor that may be developed.

Assuming that all prime movers on the hypothetical system have equal availabilities the resulting effect on the capacity-factor characteristic for the 20th unit is illustrated in Figure 4. The effect of increasing unavailability of *all* units is to tilt the characteristics, lowering the left end and raising the right end. When a unit is serving any level of the load curve which requires its operation daily its capacity factor will be decreased as a result of any extended unavailability. During the unavailable periods the next lower-efficiency unit serves in its place. This series of operating replacements follows through up to the peak-load stand-by units. In this manner it will be evident that the generation of the stand-by peak-load units will be materially increased over the condition of 100 per cent availability of all units. It is assumed that at the time of the peak load no more then one unit is unavailable and that at no time the unavailable periods of several units combine to wipe out the required operating reserve.

Prime-Mover Useful Life

The useful life of a prime mover is generally determined by one of three principal factors:

1. Wearing out of the equipment requiring unreasonable maintenance expenditures.
2. Obsolescence due to increased efficiency of modern designs.
3. Discontinuance of the enterprise which the units are serving.

The useful life is only an estimate at best, requiring:

1. Consideration of stamina of equipment relative to the severity of service to which it will be subjected.
2. A guess as to the future advancement of the art.
3. An estimate of the life of the enterprise.

In the hypothetical system under study the number of installed units needed for any given year of the system life will be fixed regardless of the range of unit life. For instance, in the 18th year of system life the total number of installed units required will be 20 in order to maintain the required reserve.

With the conception of a finite unit life it becomes possible to examine the prime-mover life capacity factor which is defined as,

$$\text{Life capacity factor} = \frac{\text{Total output during life}}{\text{total installed hours} \times \text{unit capacity}} \times 100 \qquad (19)$$

where numerator and denominator are measured in the same units. It is manifest that the life capacity factor will be the average of the annual capacity factors of a given prime mover.

Assuming a given equal life for all units in the hypothetical system it will be evident that the first three units will expire and require replacement in the same year. The three replacement units will be accompanied by an additional unit to take care of the increase in maximum energy demand. Thereafter, two units per year will have to be installed, one to replace

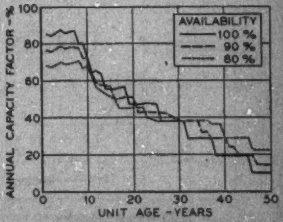

Figure 4. Change in capacity-factor characteristic of unit 20 with availabilities of all units at 80, 90, and 100 per cent

Unit life unlimited

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* The factors for the first three units are averaged on the assumption that they are of equal efficiency and will be rotated in use.

an expiring unit and the other to satisfy the load increase. Eventually the replacements for the initial three units plus the accompanying unit for load growth, a total of four units, will expire and require replacement in one year plus installation of an additional unit for growth. Thereafter three units per year will have to be installed, two to replace expiring units and one for growth. Thus definite steps are discernible in the development of the system which are marked by the increasing number of units installed per year for the same rate of load growth.

This step effect will result in the units being accelerated in their progress to the top of the load curve. Assume a ten-year life span, then by reference to Figure 5 it will be evident that the first three units will expire after the tenth year. Then in the 11th year unit 4 will be idle, unit 5 will assume the position occupied by unit 2, unit 6 replace unit 3, unit 7 replace unit 4, and so forth, and new units 13, 14, 15, and 16 will occupy the base of the load curve. A little study will show that the life capacity factor of unit 12 suffers most from this transition period of the system development from the first to the second step. Whereas, with an indefinite unit life, unit 12 would remain on the base of the load curve for at least another three years after the tenth (see Figure 1) with the ten-year unit life it has been accelerated in the 11th year to a position comparable to that of unit 8 in the tenth year or well off the base of the load curve. The units preceding unit 12 are also affected but to a lesser degree since they have served a greater portion of their life on the base of the curve. The life capacity factors of units 13 to 16 are also considerably lowered. Presumably all these units installed in the same year would be of equal efficiency and therefore rotated in actual operation whenever they are not all simultaneously needed in later years. By cross reference between Figures 1 and 5 it will become clear that in the 12th year unit 13 would be off the base of the curve, in the 13th year unit 14 would be off the base of the curve, and so forth. Hence on the average, each of these units would spend only about 2.5 years on the base compared to the expected four years if they had been installed singly in successive years.

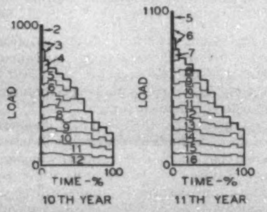

Figure 5. Comparison of annual prime-mover outputs in 10th and 11th years of system life with unit life span of ten years and availabilities of 100 per cent

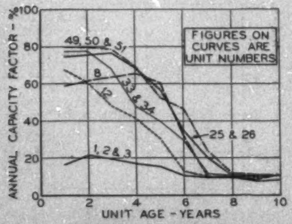

Figure 6. Maximum and minimum capacity-factor characteristics for system development steps with units of ten-year life and availability of 90 per cent

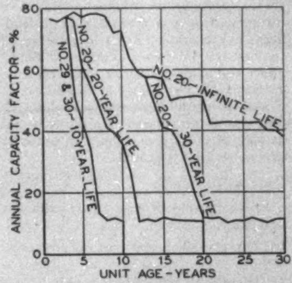

Figure 7. Capacity-factor characteristics for 10, 20, and 30-year, and infinite lives of units installed in 18th system year

Unit availability at 90 per cent

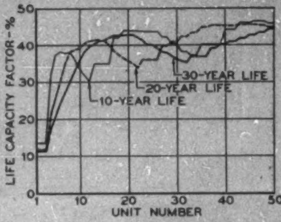

Figure 8. Variation of life capacity factor for unit lives of 10, 20, and 30 years

Unit availability at 90 per cent

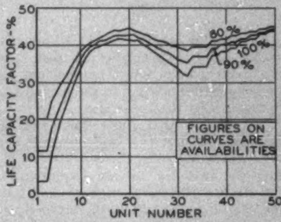

Figure 9. Effect of availability on life capacity factor of successive units having 30-year life

The units whose life capacity factors will be affected least are the units that will be accelerated in position when they are already near the top of the load curve. These will be the units that are installed about midway of each step of the system development. A comparison of the capacity-factor characteristics for unit life of ten years in Figure 6 illustrates this. Units 1, 2, and 3 are the beginning of the first step in system development, unit 8 is at the middle of the first step, unit 12 at the end of the first step, units 25 and 26 at the middle of the second step, units 33 and 34 the end of the second step, and units 49, 50, and 51 the middle of the third step. The life capacity factors of the "middle" units are higher than those of the units at the extremes of each step as will be indicated by inspection of their characteristics.

The shape of the unit characteristics behave in the same general fashion regardless of magnitude of unit life. The appearance of the relative characteristics for lives of 10, 20, and 30 years and indefinite period of a unit installed in the 18th year of system life is shown in Figure 7. In this illustration it should be noted that the unit is not installed in the same general location of system development for each life span, that is, for 30-year life the unit is installed past the middle of the first step; for 20-year life the unit is installed near the end of the first step; and for 10-year life the units are installed midway between middle and end of the second step.

The variation in life capacity factor for the units being successively added to the growing system is illustrated in Figure 8. Steps in system development are readily discernible in each of the unit life-capacity-factor curves for 10-, 20-, and 30-year life spans. The plateau formed by the initial replacement units and accompanying growth unit is prominently situated at the beginning of each step. As principally shown by the 10-year life curve the life capacity factors of the units in the same relative location of each succeeding step of system development are increasing. This is due to the increasing ratio of total system load to total system capacity with time permitting better utilization of capacity.

An unexpected effect of unit availability is the resultant variation in the unit life-capacity-factor curves shown in Figure 9 for a unit life span of 30 years. Whereas the implication of 100 per cent availability would lead to expectation of maximum utilization and therefore maximum life-capacity factor, actually the reverse is true within limits. A unit spends

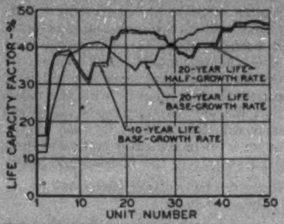

Figure 10. Effect of system-load-growth rate on life capacity factor of units with 20-year life

Availability at 90 per cent

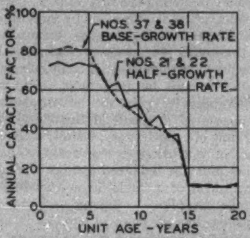

Figure 11. Capacity-factor characteristics of units installed in 27th year of system life for base and half base load-growth rates

Unit life of 20 years at 90 per cent availability

roughly 25 per cent of its life on the base of the load curve. Whatever generation it loses during this initial phase by imperfect availability is overbalanced by the generation it produces when serving as a stand-by replacement unit as a result of the unavailability of the succeeding units.

Rate of System Load Growth

The rate of system load growth affects the shape of the individual prime-mover capacity-factor characteristic and the magnitude of the life capacity factor. This effect was investigated for the units having a 20-year life span with a system load growth one-half of the base rate of growth. For this condition it is manifest that units will have to be added only every second year compared to every year for the base growth rate. The steps of system development in terms of number of years spanned is controlled exclusively by the length of prime-mover life span. However, the number of units installed per step is governed by both the rate of growth and magnitude of unit life. This can be deduced directly from the given conditions, for which the results are listed in Table II. Comparing the columns for the various lives and growth rates it will be seen that the 10-year base rate and 20-year half rate are identical and also the 20-year base rate and 40-year half rate. This suggests that the life capacity factors for units in the same relative positions on the system development steps will be equal if the product of their years of unit life and relative system load growth rate are identical, for example, 30-year base-rate units will be equal in life capacity factors to those of 60-year half-rate units.

The validity of this suggestion is borne out by the results of calculations illustrated in Figure 10. For the 20-year half-rate units the curve of life capacity factor is slightly higher than that for the 10-year base-rate units. As a specific example in the 27th year of system age units 37 and 38 are installed for the base rate of growth, whereas for the half-rate growth units 21 and 22 are installed. The life capacity factors for these pairs of units are respectively 45.3 per cent and 44.7 per cent very nearly the same. The reason for this close agreement is shown in Figure 11 illustrating the capacity-factor characteristics for these units which lie very close to each other. The initial portion of the characteristic for the units on the base-growth system is higher than that for the half-rate growth system because of the higher ratio of load to total installed capacity existing for the former condition. In the descending portion of the characteristic for the half-rate growth the jumps are caused by the increase in system load not being accompanied by increase in capacity. That is, the unit serves the same relative level of the load curve for two years in succession.

Some energy-supply systems after a period of growth may mature in that no load growth can be expected in the foreseeable future, resulting in the general load curve remaining constant in the succeeding years. For this circumstance, with an indefinite unit life, the capacity-factor characteristic of each unit would then be a horizontal line. However, for finite unit lives the units will be retired in the order of their installation, resulting in the familiar procession of the prime movers from the base to the top of the load curve. If all the individual unit lives are uniform in span of years and the capacities identical, then the capacity-factor characteristics will have the drooping shape. The life capacity factor of all units will be identical, with no system development steps appearing.

Table II. Number of Units Installed Per Step of System Development

Rate of Load Growth:	Number of Units Installed: Base Rate				Half Base Rate				System Development Step
Unit Life, Years:	10	20	30	40	10	20	30	40	
	12	22	32	42	7	12	17	22	1
	22	42	62	82	12	22	32	42	2
	32	62	92	122	17	32	47	62	3
	42	82	122	163	22	42	62	82	4

On the basis of effects of wear the unit lives most likely would be uniform. Whenever an advance in the art of energy production has been realized some of the existing units will become obsolete and an acceleration in unit retirements and additions will result in varying unit lives. This will be reflected in the capacity-factor characteristics of the existing units by their earlier arrival at the status of peak-load units.

Load Cycle and Load Factor

The nature of the load cycle and the magnitude of the load factor have a pronounced effect on the shape of the capacity-factor characteristic. In Figure 12 are shown four basic types of load cycles expressed as annual load-duration curves and the corresponding capacity-factor characteristic that may be expected of most units serving the load cycle. Figure 12A is the simplest type of cycle conceivable, a constant load for all hours of the year. The load factor, of course, is 100 per cent. If this load cycle grows at the same rate stipulated for the hypothetical system studied, the capacity-factor characteristic of most units would have the appearance shown in Figure 12A, a constantly rising curve with age concave downward, and in the last year of life dropping sharply because of only partial operation as a stand-by unit.

The reason for the major trend is apparent from the following considerations. In the fifth year of system life seven units would be required. Assuming that all units will be rotated in use, the capacity factor of the units for the year would be 5/7 = 0.714 or 71.4 per cent. In the tenth year 12 units would be required and the capacity factor of all the units would be 10/12 = 0.834 or 83.4 per cent. In the

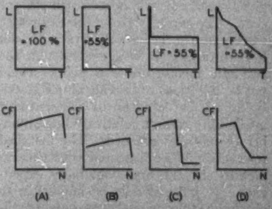

Figure 12. Effect of load cycle on capacity-factor characteristic of a unit for given unit life, availability, and load-growth rate

LF—Annual-load factor L—Load
CF—Annual capacity factor N—Years
T—Time

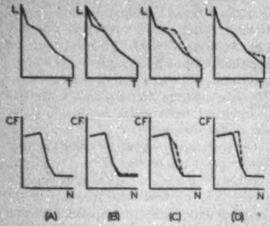

Figure 13. Effect of change in load cycle at increasing load factor on a unit-capacity-factor characteristic

CF—Annual capacity factor N—Years
L—Load T—Time

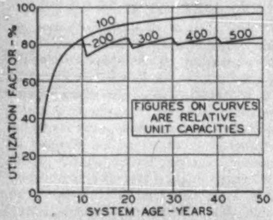

Figure 14. Effect of increasing unit capacity on system utilization factor at constant load-growth rate and ten-year unit life

15th year 17 units would be required and their capacity factor would be 15/17 = 0.882 or 88.2 per cent. Thus the annual capacity factor of the seventh unit would be as follows:

Unit Age, Years	Annual Capacity Factor, Per Cent
1	71.4
6	83.4
11	88.2
16	90.9

Obviously the capacity factor for the given conditions will never reach 100 per cent. The capacity factor increases with age because of the increasing ratio of peak load to capacity. It is evident that the two-unit installed-reserve policy would not be adequate to provide ample stand-by capacity at less than 100 per cent unit availability when a relatively large number of units is installed. The installed reserve would be increased with time as dictated by the unit-availability experience. It will generally be impossible to obtain an exact match between unit availability and maximum utilization of capacity with the result that in the last year of unit life, a unit will be only partially active because it is serving as a stand-by unit.

Figure 12B is a load cycle where the load remains constant but exists only 55 per cent of the time, thus fixing the load factor at 55 per cent. The corresponding effect on a unit capacity-factor characteristic compared to Figure 12A is to reduce the values to 55 per cent of the 100 per cent load-factor cycle characteristic.

Another shape of load cycle having the same peak load and load factor as Figure 12B is shown in Figure 12C. For the major portion of time the load remains constant but rises to the peak for periods of brief duration. The unit capacity-factor characteristic will pass through three phases:

1. When the unit is serving on the base of the load curve.

2. When the unit is serving as a replacement unit for the outage periods of succeeding units and also on peak loads.

3. When the unit is serving purely as a peak-load unit.

A load cycle of more common shape but having the same peak value and load factor as the two foregoing cycles is illustrated in Figure 12D. This is the general shape of the load cycle that has been chosen as the basis of this study. Reasons for the shape of the corresponding unit capacity-factor characteristic have already been discussed in connection with Figures 1 to 5.

Some energy-supply systems experience an improvement in load factor with a growth in load. The consequent change in a unit capacity-factor characteristic will depend on the manner in which the improvement in load factor is brought about. Figure 13A is a load cycle and corresponding unit characteristic used as a base reference. If the improvement in load factor is brought about by increasing the number of hours of operation near the peak load as in Figure 13B the capacity factor will be increased only when the unit is near the end of its life, that is, when it is serving as a peak-load stand-by unit. If the increased generation occurs at the intermediate loads as in Figure 13C the level of the descending portion of the unit characteristic will be raised. When the increased generation is brought about by raising the level of the minimum loads as in Figure 13D the base-load units serve for a longer period in that category with the result that the unit capacity-factor characteristic will be extended in time at the upper level in the early years and drop more sharply in the intermediate years.

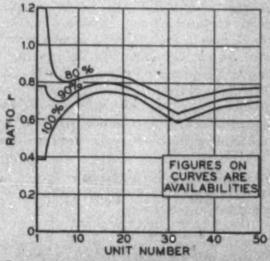

Figure 15. Variation of ratio *r* for successive units with 30-year life

Interest at eight per cent, and unit availabilities at 80, 90, and 100 per cent

RELATIVE UNIT CAPACITIES AND RESERVE POLICY

In practice it is very unlikely that the unit sizes would remain constant from the initial year of operation of a given system, especially for the constant rate of load growth assumed in this study. When completing the first step of system development as determined by individual-unit life span, four units must be installed in the initial year of the second step and two per year thereafter. For this circumstance it will be propitious to install larger units to gain the advantages of compactness of plant, lower first cost per unit of capacity, and usually a reduction in operating expenditures. These gains must be balanced against the disadvantages of a single larger-unit forced outage crippling a greater amount of capacity, less efficiency at light loads, and the need of carrying a greater amount of reserve capacity which reduces the utilization of the system as a whole.

A measure of the utilization of a system's capacity is defined as,

Utilization factor

$$= \frac{\text{maximum demand}}{\text{installed capacity}} \times 100 \quad (20)$$

The reduction in utilization factor caused by increased unit capacity for the system with units having a ten-year life is illustrated in Figure 14. The upper curve is for the condition of maintaining equal-sized units throughout. The lower discontinuous curve corresponds to installation of prime movers with relative capacities of 100 in the first step, 200 in the second step, 300 in the third step, and so forth. For the assumed conditions it appears that the utilization factor will average about 82 per cent for system age greater than 50 years.

A reduction in utilization following from increased unit size obviously must reduce the capacity factor of the prime movers involved. This is illustrated by the various conditions assumed in Table III.

During the process of system growth and development, if the size or capacity of the units added is increased over the existing units the amount of reserve capacity that must be provided is increased. This results in lowering the generation and the capacity factors of the older units under the values that would have obtained if equal-sized units had been added. Thus the general effect on the capacity-factor characteristic of a

Table III. Example of Decrease in Capacity Factor Caused by Increase in Prime-Mover Capacity for a Constant Total Relative Load of 100

Combination	Number of Prime Movers*	Relative Individual Prime-Mover Capacity	Relative Load Per Prime Mover**	Individual Prime-Mover Capacity Factor, Per Cent
A	3	50	33.3	66.7
B	2	50	25	50.0
	1	100	50	50.0
C	2	50	20	40.0
	1	150	60	40.0
D	2	50	16.7	33.3
	1	200	66.7	33.3
E	2	100	50	50.0
F	1	100	40	40.0
	1	150	60	40.0
G	1	100	33.3	33.3
	1	200	66.7	33.3

* Based on one unit operating reserve.

** Load divided in proportion to capacity.

given unit would be to lower the curve for all years during which one or more subsequent larger units are in operation nearer the base of the load curve.

Evaluation Factors

INFLUENCE OF CAPACITY-FACTOR CHARACTERISTIC

From the definition given in equations 8 and 16 it is evident that the ratio r is largely dependent on the shape of the capacity-factor characteristic that is forecast for a contemplated prime mover. V' is the weighted average of the annual capacity factors for the life of the unit. Here the present-worth factors of each year multiplied by the present-worth annuity factor corresponding to the life of the unit are the weighting elements. V_1 is then the capacity factor for the first year.

V' and the unit life capacity factor are directly commensurate since the latter is the simple arithmetic average of the unit's annual capacity factors. It follows that the behavior of r in general will pattern itself after the behavior of the unit life capacity factor as previously discussed. Whatever conditions influence the life-capacity factor will have similar effects on the value of r. In addition the ratio is also dependent upon the value of the interest rate i.

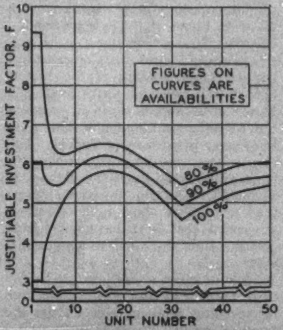

Figure 16. Variation of justifiable-investment factor for successive units with 30-year life

Interest at eight per cent, j at four per cent, and unit availabilities at 80, 90, and 100 per cent

Since the justifiable-investment factor F is dependent upon r as defined in equation 11 it also is dependent upon the capacity-factor characteristic and its controlling conditions, as well as on the fixed charge rate y.

UNIT AVAILABILITY

In Figure 15 is shown the variation in the ratio r for various unit availabilities for succeeding units having a uniform life span of 30 years when the interest rate is taken at eight per cent. Excepting the first eight or ten units the curves exhibit the same trends and relative distributions as the life-capacity-factor curves of Figure 9. As the availability of all units decreases the value of r increases, indicating that the over-all utilization of a single unit increases.

Effect of decreased availability on the first eight to ten units is unique. In the early years of their life while operating on the base of the load curve their initial annual capacity factors are relatively low (see Figure 3) but in their later years for lower availabilities of *all* units they generate a relatively greater amount of energy when acting on the intermediate loads or as peak-load stand-by units. For the first year of operation the initial annual capacity factors rise very rapidly with succeeding units, this determines the value of V_1 which accordingly will rise very rapidly in value for the first eight to ten units in turn. The relationship of the first-year capacity factor V_1 and the weighted life-capacity factor V' is markedly affected by decreasing unit availability causing a very rapid increase in the value of r. For the units following the first ten the initial annual capacity factor V_1 rises at a comparatively slow rate which allows the value of the weighted annual capacity factor V' (or in effect the unit life capacity factor) to determine the trend of the r curves.

Figure 16 shows the effect of unit availability on the variation in justifiable investment factor F for succeeding units of 30-year life span, taking $i=8$ per cent and $j=(^1/_2)i=4$ per cent. Excepting the difference in ordinate values the curves are a duplicate in general appearance of the r curves in Figure 15. From a study of Figure 16 it is manifest that within the limits of the availabilities shown more money can be justifiably invested in a given unit the less the availability of all the individual units. In particular, it is the possibility of improvement in availability of the *future units not yet actively under consideration* that must be considered. As the future units improve in availability through decreased accidental outages and lesser need for inspection and general overhaul outages the unit now being installed will have less opportunity to serve actively as a stand-by unit during such outages and therefore its life capacity factor will shrink. This circumstance would favor the proposals requiring less investment for a new unit being installed now.

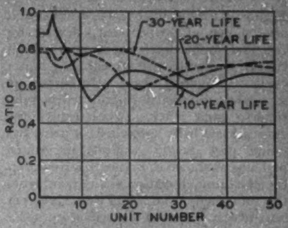

Figure 17. Variation of ratio r for successive units with lives of 10, 20, and 30 years

Interest at eight per cent and availability of 90 per cent

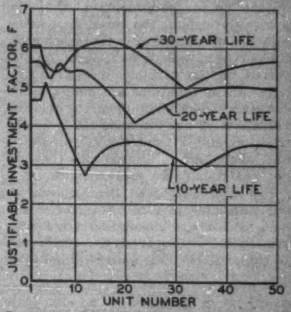

Figure 18. Variation of justifiable-investment factor for successive units with unit lives of 10, 20, and 30 years

Interest at eight per cent, j at four per cent, and availability at 90 per cent

Expected availability of future units is an especially critical factor for a new energy-supply system that is expected to expand indefinitely in the future. If the expected availability is high a relatively cheap initial installation should be made because of the rapid relegation of this installation to peak-load service and very little operation as stand-by. Conversely, for a low availability a more expensive plant is justified because of its relatively great activity as a stand-by unit in the future.

UNIT LIFE

Variation in the ratio r for unit life spans of 10, 20, and 30 years is shown in

Figure 17 for a value of $i=0.08$ and a unit availability of 90 per cent. Except for the first eight units the curves exhibit trends similar to the life-capacity-factor curves in Figure 8, though the curves are somewhat differently distributed in relation to each other. In Figure 17 the curves have been smoothed; a point-by-point plot would exhibit the same plateaus as shown in Figure 8. However, the maximum deviation of any point from the smoothed curve is less than three per cent which is within a satisfactory order of precision for economic evaluations.

Secondary cyclic variations that are quickly damped out are very marked in each of the curves in Figure 17 for the first eight units. These variations are discernible in the original plot of the points throughout the curve, but, being minor in magnitude after the eighth unit, the curves have been smoothed. As discussed in the previous section of the paper these variations are purely a circumstance dependent upon the relative variations in the capacity-factor quantities V_1 and V' and the magnitude of unit availability. Beyond this they do not seem to have any great significance.

Figure 18 illustrates the variation in the justifiable investment factor F for unit lives of 10, 20, and 30 years with availability at 90 per cent, $i=0.08$, and $j=(^1/_2)i=0.04$. The longer the life of the units the less the depreciation rate and the fixed-charge rate and the greater the investment that is justified. Accordingly the general level of the curves indicate this, the shortest life being at the bottom and the longest life at the top.

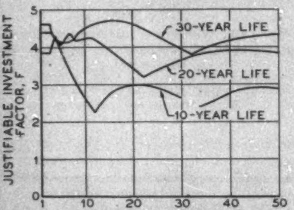

Figure 19. Variation of justifiable-investment factor for successive units with unit lives of 10, 20, and 30 years

Interest at eight per cent, j at eight per cent, and availability at 90 per cent

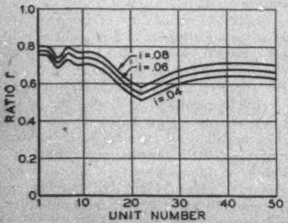

Figure 20. Variation of ratio r for successive units with 20-year life at interest rates of four, six, and eight per cent

Availability at 90 per cent

Excluding the first half of the first step in system development for each curve it becomes evident that at or near the terminal points of each step the magnitude of justifiable investment is a minimum. This follows from the minimum utilization of the equipment caused by its acceleration in progress to the top of the load curve, that is, it serves a minimum of time as a base-load unit compared to other units on the system. The units installed at or near the middle of a step permit a maximum of justifiable investment because of the maximum utilization made of their capacity during their life, that is, they serve for the maximum possible length of time on the base of the load curve.

For a new energy-supply system, the important influence of unit availability has already been emphasized in referring to Figure 16. If 90 per cent is taken as a reasonable value of unit availability then from Figure 18, for the 10- and 20-year life spans, the initial units enjoy the highest values of F. This is substantially true for the 30-year life span except for the rapid dip in value of F for the units 4, 5, 6, 7, and 8. However, the value of F for these units is still higher than its magnitude at succeeding terminal points of system-development steps. If the availability becomes greater than 90 per cent the values of F will accordingly drop very markedly for the initial units.

INVESTMENT CHARGES

The investment charges as determined by the fixed charge rate are made up of three principal categories:

1. Interest.
2. Depreciation.
3. Taxes and insurance.

Depreciation as treated in this paper is principally determined by unit life. Though the r curves in Figure 17 are closely grouped, the corresponding F curves in Figure 18 are distinctly separated. This is caused by the difference in the depreciation rate for the different unit-life spans.

The effect of taxes and insurance is shown in the comparison of Figures 18 and 19. In the former the sum of the tax and insurance rates j is assumed to equal one-half the interest rate, in the latter the sum is equal to the interest rate. This doubling of the tax and insurance rate total causes a decrease in the justifiable investment for all lives and makes the curves draw closer together. This effect is in keeping with the general principle that the magnitude of the optimum investment is decreased as the annual expenditures directly incurred by the investment are increased.

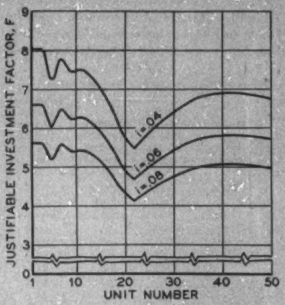

Figure 21. Variation of justifiable-investment factor for successive units with 20-year life at interest rates of four, six, and eight per cent

Availability at 90 per cent and j at four per cent

In Figure 20 the effect of the interest rate is shown in fixing the magnitude of the ratio r for succeeding units having a 20-year life span and an availability of 90 per cent. The curves are practically equidistant and the value of r rises as the interest rate increases. In Figure 21 the corresponding effect on the justifiable investment factor F is shown for the same unit conditions. The curves are not equidistant and the progression is opposite to that of the ratio r. As the interest rate increases the factor F decreases. This is in accord with the general principle that as the cost of money use increases the investment of the optimum plant decreases, other conditions remaining constant.

RATE OF LOAD GROWTH

Effect of the rate of load growth on the life capacity factor has been illustrated in Figure 10. The corresponding effect for the same conditions on the ratio r is shown in Figure 22. Coincidence of the development steps of the 10-year base-growth rate and the 20-year half-growth rate units is again in evidence, but, the difference in ordinate value of the two curves is much more marked. This carries over with increased accentuation in the difference of the factors F shown in Figure 23. The two 20-year life curves tend to oscillate about each other, but in general they both have appreciably greater values than for the ten-year life units.

For the given conditions the initial three units show the greatest variation of the value F (about 1 to 1.4) for the two load-growth rates. About 1 to 1.3 is the maximum variation for later units, specifically unit 22. For other proportions of the base-growth rate of the load with a given unit-life span it is evident

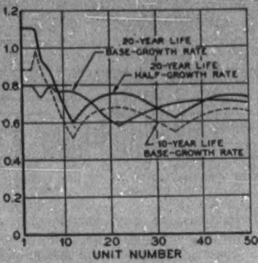

Figure 22. Effect of system-load-growth rate on ratio r for successive units with 20-year life

Interest at eight per cent and availability at 90 per cent

that a net of curves fluctuating within the limits of the maximum and minimum values shown for the two given rates will exist.

SYSTEM LOAD CYCLE AND LOAD FACTOR

Effects of load cycle and load factor on the capacity-factor characteristic of a unit have been illustrated in Figures 12 and 13. Corresponding effect of the load cycle on the ratio r and factor F for successive units added to a growing system is shown in Figure 24. For the 100 per cent-load-factor continuous load in Figure 24A the ratio r starts at some value less than 2.0 for the initial units installed, after which it drops rapidly for the next few units and then oscillates somewhat erratically about (but very close to) unity for the succeeding units. Fluctuations in the value of F follows the same trend as for r but of course the ordinate value will depend on the interest and fixed-charge rates.

With the intermittent load of fixed magnitude in Figure 24B the ratio r and F behave identically with the trends in Figure 24A for the continuous constant load of same magnitude. This contrasts with the difference in the general magnitude of the capacity-factor characteristic of any unit for the two loading conditions as shown in Figures 12A and 12B. The identity of r and F follows from the same shape of the capacity-factor characteristics. If the interest rate, unit life, availability, and fixed charge rate are the same for the two load cycles r and F will have identical ordinate values for each unit.

For the load cycle in Figure 24C the variations in value of r and F take on the characteristics of the fluctuations exhibited for the common shape of load curve as in Figure 24D which have been discussed at length. These variations are the direct result of load curves having more than one magnitude of load. The ordinate values of r and F in the latter two illustrations will depend upon the value of the controlling conditions cited in the previous paragraph as well as the proportions of the load-duration curve.

Figure 13 illustrates the effect of improving load factor on the capacity-factor characteristic of a given unit. From a study of this figure it is evident that the ratio r and the factor F will be increased to a maximum extent when improvement in load factor occurs by increasing the magnitude of the minimum load; the least increase will result from increased operation at the peak loads. This follows from the small weight given the annual capacity factors of a unit in the final years of its life as determined by the magnitude of the present worth factors used in determining the ratio r.

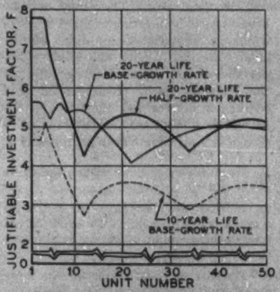

Figure 23. Effect of system-load-growth rate on justifiable-investment factor for successive units with 20-year life

Interest at eight per cent, j at four per cent, and availability at 90 per cent

Conversely, the extension of operation at relatively high capacity factors in the early years of a unit's life will accordingly increase the magnitude of r and F.

RELATIVE UNIT CAPACITIES AND RESERVE POLICY

In the previous discussion it was demonstrated that an increase in unit capacity causes the operation of a greater amount of operating reserve (see Table III) and a lowering of all the capacity factors of the units involved. Any lowering of a unit's annual-capacity factor after its first year of operation will lower the value of r and F. Then for a new unit to be currently installed it will be necessary to estimate the time at which larger subsequent units may be installed and to discount accordingly the magnitude of the evaluation factors.

Application to the Practical Case

CONTROLLING FACTORS

It is readily recognized that the assumption of values for the various controlling elements that must be made for an economic evaluation may vary widely from the magnitudes that are eventually realized. In view of this insurmountable hazard the foregoing discussions may appear to be only of academic interest. However, this situation is an inherent condition of all new business ventures and usually it is only after a thoroughgoing study of all elements and probable future events prove favorable that an enterprise is undertaken. As an aid to such thoroughgoing study the subject of this paper is valuable for expanding systems of energy supply; since the relative importance of the various controlling conditions have been clearly high lighted.

Constancy of the various controlling conditions assumed in the study of the hypothetical system is highly unrealistic in most instances. However, this artificial assumption is the only means available to enable determination of cause and effect. General conclusions as to the practical effect of the various controlling conditions are drawn in the following:

(*a*). *Unit Availability*. At this writing the availability of some types of prime movers seems to approximate 90 per cent. How close 100 per cent availability may be approached in the future is debatable. From Figure 16 it appears that for an established system the justifiable investment factor may increase by about five to nine per cent in magnitude when unit availability decreases from 100 per cent to 90 per cent. Thus it is of small moment whether 90 per cent or 95 per cent is assumed for future unit availability because of the relatively small effect on the magnitude of F. However, for the initial installations of a growing system and those immediately following the unit availability is of primary importance because of its effect on the activity of these units in their later life. The critical importance of availability seems to disappear when system-load-growth rate becomes about 15 per cent or less.

(*b*). *Unit-Life Span*. The interplay of fixed unit-life span and uniform load growth causes certain predictable units to have either high or low investment factors F. In actual cases neither unit life span or load growth is uniform. In the past, unit life has more often been determined by obsoles-

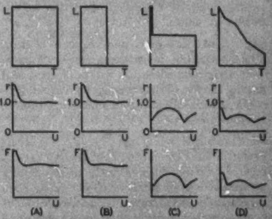

Figure 24. Effect of load cycle on variations in r and F for successive units at given magnitudes of availability, unit life, and fixed-charge rate

L—Load
U—Successive units
T—Time
r—$\frac{V'}{V_1}$
F—Justifiable-investment factor

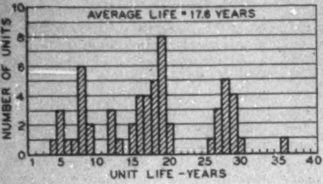

Figure 25. Life span of steam-engine and turbine-driven generating units retired during the years of 1898 to 1943 on the system of the Consolidated Edison Company of New York, Inc.

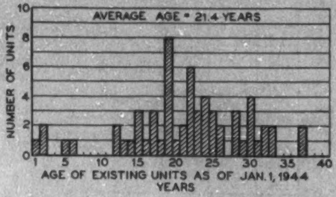

Figure 26 (right). Age as of January 1, 1944, of existing turbine-generator units on the system of the Consolidated Edison Company of New York, Inc.

cence rather than by the physical ability of existing equipment to carry on in service. The appendix exemplifies the wide range in prime-mover life that has been experienced on a large growing system. When a new unit is being considered for installation two elements of the situation should be critically examined:

1. The physical condition of the peak-load units to determine when they might need replacement due to deterioration.

2. The possibility of advancement in the art making it worth while to install new equipment during the life of the contemplated unit.

If new units for either cause seem likely then the justifiable investment factor will tend to be near a minimum value. If installations succeeding the contemplated unit will be only for load growth, then the factor *F* will be near the maximum value.

(*c*). *System Load Cycle and Load Factor.* The nature or shape of the load cycle is of primary importance in determining the capacity-factor characteristic of a unit, the flatter the shape of the load curve the nearer the capacity-factor characteristic approaches a horizontal line and the nearer the investment factor *F* approaches $1/y$. Again, the several initial and early units are the exception for which the characteristics may be of a general rising trend and the values of *F* lie between $1/y$ and $2/y$. An improvement in load factor causes a maximum increase in *F* if it is brought about by increasing the minimum loads of the load cycle; a minimum increase in *F* results if the load factor is improved by extended operation at the peak-load value.

(*d*). *Unit Capacity and Reserve Policy.* The justifiable investment factor *F* of a unit under consideration will be decreased if during the life of that unit an increase in subsequent unit capacities or operating reserve is likely to occur.

(*e*). *Rate of System Load Growth.* It seems probable that the most likely growth experience will be one where there is none initially or only at a very small rate. The rate then increasingly accelerates till a condition of constant growth is reached for an extended period after which it tapers off to a small value or perhaps disappears altogether. The general range of magnitude of the factor *F* is principally determined by the life span of the unit and the interest rate, but the rate of system load growth will determine where a given unit will be located in the range of values of *F*.

RECOMMENDED PROCEDURE FOR DETERMINATION OF EVALUATION FACTORS

Direct application of the curves developed in this study cannot be made to any practical case. They may only be used as a guide in estimating the effect of a change in initial assumptions on the answer desired. With the relative importance of the various factors established the following is a recommended procedure for determining the value of the justifiable investment factor *F* for addition of any prime mover to any system:

1. Establish the present annual-load curve of the system.

2. Forecast the peak load of the system for a period at least equal to the expected life span of the new unit by any rational means that are available.

3. Forecast the load factor for the same period and determine at what levels the annual-load-curve shape is altered if the load factor changes. Also investigate the possibility of alterations in shape with a constant load factor.

4. Review the operating- and installed-reserve-capacity policy to establish any changes that may be necessary with a higher load.

5. Review the existing prime movers and determine the time at which they will have to be retired due to physical deterioration if this appears necessary.

6. Estimate the likelihood of any advancement in the art that may justify replacing any of the present equipment due to obsolescence.

7. Estimate the optimum capacity of the future units required in keeping with the load prediction and installed reserve policy.

8. Estimate the availability of the future units.

9. With the foregoing data in hand set up the load division among units for each year at 100 per cent availability.

10. Tabulate the annual total output at 100 per cent availability for each unit and each year under study.

11. Set up an adjusted tabulation from item 10 of unit annual output corrected to expected unit availabilities.

12. From the annual outputs or capacity-factor characteristic determined in item 11 for the unit under study find the ratio *r*.

13. Knowing *r* and *y* determine *F*.

Manifestly some of the recommended estimations are mere guesses at best. However, some limitations on the reasonableness of the guess will generally be apparent. In such circumstances it may very well be worthwhile to set up a study for each limit of the guess to arrive at the effect on the final answer.

Appendix

An example of the difficulty of predicting the actual life span of a prime mover is shown in the frequency chart of Figure 25. Here are the life spans of 58 units, steam-engine- and steam-turbine-driven generators, classified according to the number of units having each nominal number of years of service. The range varies from a minimum of 4 years to a maximum of 36 years. Records are meager, but it is very likely that most if not all these units were retired because of obsolescence rather than functional depreciation. This is certainly true of the units having very short lives, most of which were steam-engine-driven sets that were rapidly retired when the advantages of the steam-turbine were undeniable for central-station service. For these 58 units the average life span was 17.6 years. Of interest is the following tabulation showing the increasing life span of the units with advancing time:

Period	Number of Units Retired	Average Life Span, Years
1898–1910	5	5.0
1911–1920	21	13.1
1921–1930	7	14.6
1931–1943	25	24.6

Contrasting with these average life spans is the average age of the existing 61 units on this system which is 21.4 years. The range of ages varying from one to 37 years, as of January 1, 1944, is illustrated in Figure 26. The unusually large number of units of like ages, for example the 19- and 22-year age groups, follows from the fact that the present system represents a consolidation of at least five previously independent systems. These units were installed in the same year for load growth of these independent systems and were in no way economically interrelated at the time.

Reference

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Fault Protection on Shipboard A-C Power-Distribution Systems

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THE growth in the use of electrically driven auxiliaries on shipboard has produced very large increases in the installed generating capacity. This fact together with the necessity for maintaining a high degree of reliability and continuity of service in shipboard power-distribution systems has emphasized the importance of providing adequate fault protection. Such protection is concerned primarily with the automatic isolation of a fault with minimum damage and with minimum disturbance to the remainder of the system. It is the purpose of this paper to develop some of the fundamental considerations in fault protection on shipboard and to establish the basic principles and methods for the selection and arrangement of the various protective devices to achieve a complete and coordinated protective system. The discussion will be restricted to low-voltage (600 volts or less) a-c systems although many of the principles are also equally applicable to d-c systems.

A typical shipboard power-distribution system is shown in Figure 1. It is essentially a radial-type system with provisions for interconnecting the power sources by means of bus-tie circuits between each main switchboard. Power is generated and distributed at 450 volts, three phase, 60 cycles. The system is delta-connected and ungrounded. When compared with the power-generation and distribution systems usually encountered in shore practice, the shipboard power system is evidently relatively simple and straightforward. Nevertheless the relatively close electric coupling between the generators and the loads, the high decrements in the short-circuit currents, and, particularly, the requirements of marine service have created a number of special problems in connection with the provision of an adequate protective system. Moreover, it has been found that the conventional methods of protection for low-voltage power systems are either inadequate or not suitable for shipboard application because of restrictions on space, complexity, and maintenance, and the need for a protective system with a high degree of reliability.

In considering the problem of protection on a low-voltage power-distribution system it is essential at the outset to recognize the distinction between the purposes of system fault protection and thermal overload protection. The former is concerned primarily with the isolation of faults from the system, that is, protection of the system from experiencing interruptions of service due to faults on individual circuits or in apparatus, whereas the latter type of protection is directed towards preventing excessive temperature rise in apparatus due to overloads. To some degree the two types of protection overlap in that an apparatus may be thermally damaged by the flow of fault current through it as in the case of a fault on the load side of a motor controller. However, it is essential to the development of a fault-protective system that the function of fault isolation remain uppermost. Thus generator circuit protection should be concerned with the isolation of faults in the generator or generator cables and the minimizing of damage or fire hazard from bus faults and should not attempt to provide automatic thermal protection. The latter should be secured by relying on manual supervision to avoid excessive loading.

Basic Requirements

A fault-protective system for shipboard power-distribution systems should possess the following characteristics:

1. High-speed clearing of faults.
2. Selective operation under fault conditions.
3. Maximum protection of electric apparatus and circuits.
4. Adequate interrupting capacity in all circuit-interrupting devices.
5. Adequate thermal capacity in all circuit protective devices.
6. Simplicity of operation and ease of maintenance.

Although many of these requirements are common to shore power systems it is desirable to review each of them with particular reference to conditions encountered on shipboard.

HIGH-SPEED CLEARING OF FAULTS

High-speed operation of circuit protective devices is essential, particularly on all low-impedance faults, in order to minimize damage to the system and apparatus under fault currents of large magnitude. On low-voltage systems of even moderate capacity the fault currents may be in the order of 25,000 amperes, while in some cases currents above 50,000 amperes are possible. The large amount of energy which thus is liberated in the vicinity of a heavy fault constitutes a definite fire hazard. High-speed operation of the protective devices will reduce the likelihood of serious damage from this source. In addition, reducing the fault-clearing time to a minimum is also desirable to avoid excessive loss of load due to the low-voltage conditions existing during the presence of a fault.

Low-voltage air circuit breakers have operating times of approximately one to three cycles depending on their rating and also to some degree on the magnitude of current flowing in those cases where the breaker is tripped by means of a series overcurrent coil. Fuses on the other hand are thermally actuated devices and therefore have operating times which are entirely a function of the fault current. Unless this current is 30 to 40 times the ampere rating of the fuse its interrupting time may become excessive. For this reason the maximum fuse rating which may be used for fault protection is governed by the available short-circuit currents. On shipboard low-voltage power systems it is desirable to limit fuse protection to circuits where the fuse size required does not exceed 200 amperes.

SELECTIVE OPERATION

The maintenance of a high degree of service continuity is of vital importance on shipboard power-distribution systems. It is therefore essential that, insofar as practicable, only the circuit on which a fault occurs be disconnected automatically from the system, thus allowing the remaining circuits to continue in service without interruption. The selective operation thereby required of the various circuit protective devices is one of the major problems in the design of a protective system and will be discussed in more detail later.

MAXIMUM PROTECTION

High-impedance faults are quite likely to occur on shipboard power systems because of the relatively low voltages at which these systems operate. Although such faults will either burn clear or develop into low-impedance faults after a short time, nevertheless it is desirable that the protective system be designed to operate over a wide range of fault impedance. Moreover, in order to minimize damage to apparatus such as motor controllers and cables under fault conditions, the tripping characteristics of the circuit protective devices should be coordinated with the thermal characteristics of the circuit or apparatus.

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ADEQUATE INTERRUPTING CAPACITY

The large increases in generating capacity on shipboard power systems makes the determination of the maximum short-circuit currents and the provision of interrupting devices of adequate capacity a major factor in the power-system design. Severe damage and fire may occur as the result of the failure of a circuit interrupter when operating at currents above its interrupting rating. Because of the restricted space available on shipboard, the use of backup protection is often necessary. This protection consists of connecting a circuit-interrupting device of adequate interrupting capacity ahead of other protective devices of insufficient rupturing ability to ensure clearing the maximum short-circuit current and to minimize damage to the latter devices. However, since backup protection necessitates loss of continuity of service and, in most cases, damage to the device protected, its use should be restricted as much as possible. Furthermore, it should be noted that backup protection of a fuse by means of a circuit breaker is of limited value because of the extremely high-speed operation of the fuse at currents above its interrupting rating.

ADEQUATE THERMAL CAPACITY

Although not often taken into account in low-voltage practice, the short-time current rating of protective devices and switching equipment must be recognized if satisfactory system protection is to be secured. This factor is particularly important in a protective system wherein the requirements for selective operation may result in the introduction of intentional time delay in the clearing of faults. Moreover, all interrupting devices have some inherent time delay before opening the circuit and thus impose a definite short-time duty on all apparatus.

SIMPLICITY OF OPERATION AND EASE OF MAINTENANCE

The exigencies of marine service do not permit the use of elaborate protective systems. The generally greater care required to maintain such systems at maximum operating effectiveness is neither possible nor desirable. For this reason and from considerations of optimum reliability it is preferable that all circuit protective devices be of the direct-acting types having all protective functions integral with the circuit interrupter and with minimum dependence on external devices. Thus the use of relays should be reduced to a minimum because of their usually greater complexity and the necessity of obtaining tripping energy from an external source.

Methods of Securing Selective Operation

It is evident from the foregoing discussion that one of the principal considerations associated with providing fault protection on shipboard power systems is that of securing selective operation of the various protective devices. Such operation is made difficult primarily by

1. The relatively close electric coupling or lack of impedance between the various important sections of the power system.
2. The high decrement of the short-circuit current output of the generators.

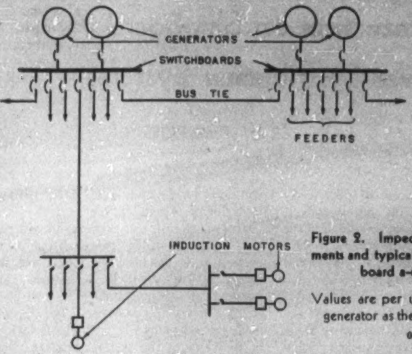
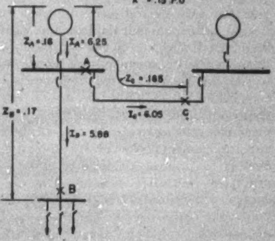


Figure 1. Schematic diagram of a typical shipboard a-c power-distribution system

Figure 2. Impedance of essential elements and typical fault currents on shipboard a-c power system

Values are per unit with rating of one generator as the kilovolt-ampere base



The former condition is immediately clear from Figure 2 which shows the impedance of some of the principal elements of a typical system. It is evident that a fault at point *A* or *B* draws approximately as much current as a fault at point *C*. As a consequence there is no possibility of determining the location of the fault on the basis of the magnitude of current alone. The high-decrement factor existing in the short-circuit current of a typical generator for marine use is shown in Figure 3. This phenomenon makes securing selective operation combined with high-speed operation by means of conventional methods extremely difficult if not impossible.

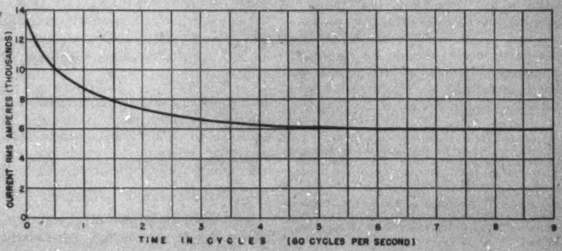
There are several methods available for securing selective operation of circuit protective devices under conditions where differentiation between fault locations cannot be made on the basis of fault-current magnitude alone. These methods are:

1. Current differential.
2. Directional overcurrent.
3. Inverse-time-delay overcurrent.

CURRENT-DIFFERENTIAL PROTECTION

Current-differential protection consists in balancing the currents flowing into and out of a given section of the power system or a given unit of apparatus. Figure 4 shows a typical current-differential scheme consisting of an overcurrent

Figure 3. Typical short-circuit current versus time curve of a-c generator for marine service equipped with automatic voltage regulator



relay and two current transformers, one transformer located at each end of the section to be protected. For external faults the secondary currents of the transformers are balanced and no current flows in the relay. When a fault occurs within the section the transformer currents are unbalanced causing current flow through the relay which in turn trips the circuit breaker. This type of protection is inherently selective and can be operated without intentional time delay.

It would seem at first glance that current-differential protection would be ideally suited for shipboard use. However, there are a number of reasons which argue against its use as a general rule. First, to be effective in providing complete system protection current-differential protection must be used for more than one section of the system. This fact is shown in Figure 5 where it is evident that in order to use differential protection on the bus tie or generator circuits it is also necessary to provide differential protection on the main-switchboard bus if bus faults are to be cleared. Since the latter are among the most common and the most damaging types of faults which occur on shipboard it is essential that they be included in the protective system. However, in general, while differential protection has been used widely for this purpose in shore practice, the large number of feeder circuits and consequent complexity of the control wiring incident to its use on shipboard make it undesirable for bus fault protection. Similarly, it is obvious that differential protection cannot be used on shipboard feeder circuits because of the excessive amount of external pilot wiring required to interconnect the two ends of the circuit plus the large number of relays and resulting complexity and reduced reliability of such a system.

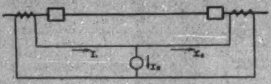


Figure 4 (left). Essential components of current-differential protection for a circuit element

$$I_R = I_1 - I_2$$

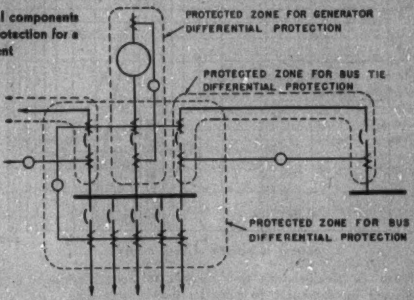

PROTECTED ZONE FOR GENERATOR DIFFERENTIAL PROTECTION
PROTECTED ZONE FOR BUS TIE DIFFERENTIAL PROTECTION
PROTECTED ZONE FOR BUS DIFFERENTIAL PROTECTION

Figure 5. Application of current-differential protection to bus tie, generator, and main bus showing co-ordination of protective zones

DIRECTIONAL OVERCURRENT PROTECTION

Directional overcurrent protection is applicable to loop circuits wherein power is supplied at only one point. In loop circuits where power is fed in alternately at several points such as occurs on shipboard bus-tie systems the use of directional overcurrent protection becomes too complicated if not impossible. However, this type of protection can be used to advantage on generator circuits to ensure isolation of faults in the generator or generator cables. In such applications a reverse-current relay can be used set at the minimum current and time permissible to avoid false tripping due to synchronizing power transients. By this means selective operation between the generator circuit breaker and other circuit breakers in the system for faults in the generator circuit can be achieved with little difficulty. Whether or not such protection should be used depends largely on the likelihood of such faults occurring. In general, if the generator cable connections are long, reverse-current protection of the generator circuit may prove to be desirable.

INVERSE-TIME-DELAY OVERCURRENT PROTECTION

The most commonly used method of securing selective operation of circuit protective devices on a low-voltage radial system under fault conditions is by means of inverse-time-delay overcurrent trip devices. The latter have usually been fuses, induction-type relays, or circuit breakers equipped with oil-film dashpots. Each of these devices has definite limitations insofar as meeting all of the requirements for fault protection set forth herein. Moreover these limitations exist both on shore installations and on shipboard.

There are three major disadvantages in the use of fuses for circuit protection on shipboard:

1. The necessity for fuse replacement following its operation in clearing a fault and the attendant delay in restoring service make it desirable to restrict their use to circuits of a nonvital nature.

2. The lack of precision and consistency in the time–current characteristic of the ordinary fuse makes the proper co-ordination of several fuses connected in series extremely difficult.

3. High-speed fault protection cannot be secured except on low-capacity circuits because of the inherently long time delay of fuses of large ampere rating and the high decrement on the generator short-circuit characteristic.

If we refer to Figure 6, it is evident that, while the initial value of the short-circuit current, if maintained, would have resulted in the fuse blowing in 0.10 second, the actual time to clear the fault is increased to 0.40 second. This slower speed of operation is accentuated when several fuses are connected in series having increasing time delays.

The chief disadvantages in the use of relays on shipboard for inverse-time-delay overcurrent protection are

1. Securing a suitable source of tripping energy.

2. Excessive switchboard space requirements.

These difficulties are also equally applicable to the use of relays in the current-differential and directional overcurrent types of protection previously discussed. The extremely low voltages which are likely to exist under fault conditions make it impossible to rely on the a-c system voltage for tripping the circuit breakers. For this reason it is necessary to use a separate voltage source for this purpose. Accordingly, when relays are used for circuit protection on shipboard, it has been customary to trip the circuit breaker by means of voltage from the generator exciter because of the reliability of this source.

It is evident that the difficulties of providing a suitable source of tripping energy preclude the use of overcurrent relays on feeder circuits, particularly where more than one generator may be used to energize the switchboard as is usually the case on shipboard. Moreover, the large number of relays generally required does not make them suitable for such an application in any case because of space limitations. However, on bus-tie and more particularly on generator circuits overcurrent inverse-time-delay relays can be used without any of these difficulties and consequently they have had extensive use on shipboard for this purpose.

Oil-film dashpots in conjunction with a direct-acting trip device actuated by a series overcurrent coil have been the standard method of securing inverse-time overcurrent protection on low-voltage air circuit breakers for many years. Such devices have the advantage of simplicity in that they require no external tripping

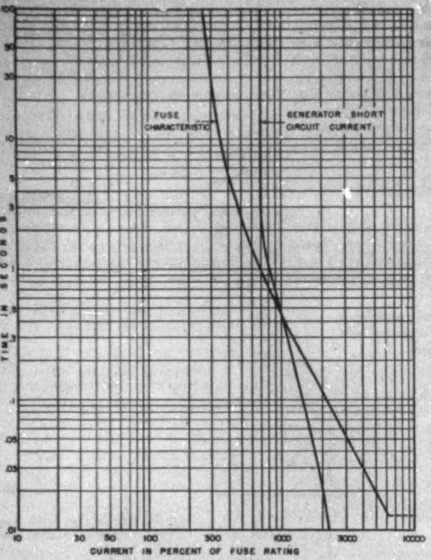


Figure 6. Fault-clearing time of fuse as affected by decrement in generator short-circuit current

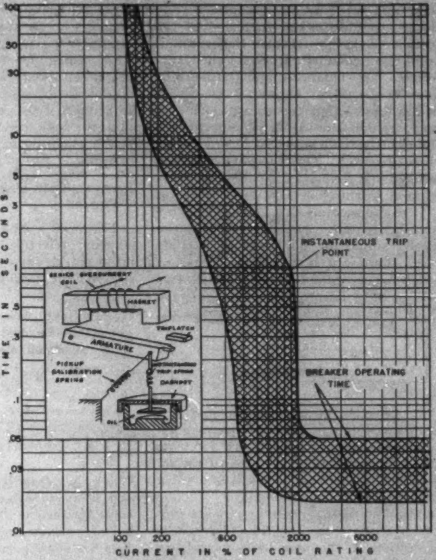


Figure 7. Essential elements of direct-acting oil-film inverse-time-delay device for air circuit breakers and time–current characteristic

source or current transformers. However, they are not suitable for securing selective operation on shipboard power systems because an oil-film timer is inherently incapable of giving sufficiently accurate time delay to permit the coordination of the tripping characteristics of several circuit breakers connected in series.

The essential elements of an oil-film time-delay device are shown schematically in Figure 7 together with a typical time–current-characteristic curve. From a study of this figure it is evident that the variations in tripping time for current above 300 to 400 per cent of the pickup current are too great for reliable selective operation. Moreover the presence of a small amount of dirt in the dashpot or a scratch on the surfaces of the disks makes the performance extremely erratic, and as a result even greater departures from the nominal time delay shown by the curve of Figure 7 usually occur.

There is an additional shortcoming of inverse-time-delay overcurrent protection over and above the mechanical and operational deficiencies of the devices used and that is the unsuitability of the standard time–current-characteristic curve of an inverse-time-delay device. This factor already has been mentioned briefly in connection with fuses, but it is considered worth while to analyze it more fully at this time.

On shipboard power systems the locked-rotor currents of many of the motors are often equal to 50 to 100 per cent of the rated current of one generator and three or four times the rated current of the feeder to which the motor is connected. As a consequence, the time–current characteristic of the circuit protective device must bear a definite relationship to the starting current of the motors in order to avoid false tripping under starting conditions. Figure 8 shows the starting current of a typical induction motor as a function of time. To coordinate with this the time–current characteristic of a circuit protective device must be similar to either curve *A* or curve *B*, that is, it must introduce longer time delays at corresponding current values or must pick up at a current greater than the maximum value of the motor-starting current. Since the latter may become very large with respect to the normal load current of the circuit, it is usually undesirable to use the higher pickup setting shown in curve *B*, because of the possibility of failing to clear high-impedance faults. However, it is also evident that the characteristic of curve *A* results in excessively long clearing times at high values of fault current, particularly when considered in relation to the high decrement in the short-circuit characteristics of the generator as shown by curve *C*. It is evident, therefore, that the ideal inverse-time–current characteristic is obtained by a device which gives the combined functions of curves *A* and *B*, in other words one introducing relatively long time delays at currents up to 300 to 400 per cent of the circuit rating and very short time delays for currents above this value. Finally, in order to provide backup protection at values of current above the interrupting rating of other circuit protective devices in the system and, because, of the limited short-time current rating of low-voltage air circuit breakers, the time–current characteristic must become instantaneous at a definite current value. The characteristic curve of a device having these combined functions is illustrated in Figure 9.

For convenience in identification and subsequent discussion of its application the time–current curve of the direct-acting inverse-time-delay circuit-breaker overcurrent trip device shown in Figure 9 and previously described has been divided into three types of protection:

1. *Time-Delay Overload Protection.* This term applies to that portion of the curve wherein relatively long time delays are introduced at overcurrents of relatively small magnitude. It should be noted that it is directed towards high-impedance fault protection and not overload protection.

2. *Time-Delay Short-Circuit Protection.* This term applies to that portion of the characteristic covering the current range in which most short circuits will fall and in which the minimum time delay necessary for selective operation is provided.

3. *Instantaneous Short-Circuit Protection.* This term applies to the provision for tripping the circuit breaker with no purposely introduced time delay and is needed primarily either for backup protection or to prevent exceeding the short time-current rating of the breaker.

One further factor must be considered before satisfactory selective operation can be obtained when using inverse-time overcurrent trip devices of the direct-acting type on circuit breakers. This factor is that of the ability of the device to reset without tripping after the short circuit has been cleared by some other breaker in the circuit. The nature of this problem can be seen by reference to Figure 10A which shows the magnetic circuit consisting of a series overcurrent coil and armature for a typical direct-acting trip device. It is evident that some movement of the armature will be necessary in order to introduce time delay before tripping. The consequent reduction in the length of the air gap in the magnetic circuit coupled with the inherent dissimilarity between the armature and spring-pull characteristics means that less current is required to overcome the pull of the pickup spring, as shown in Figure 10B. Under short-circuit conditions, therefore, unless care is taken to ensure that the reduction in air gap while the fault current is flowing is sufficiently small the breaker still may follow through and trip because of load current even though the breaker beyond has already cleared the fault. To minimize the change in air gap a suitable margin therefore must be provided between the time delays of successive breakers, and this should include the interrupting time of each circuit breaker. In the subsequent discussion on the application of inverse-time overcurrent protection it should be understood that this factor of reset ability must be included in co-ordinating the protective devices.

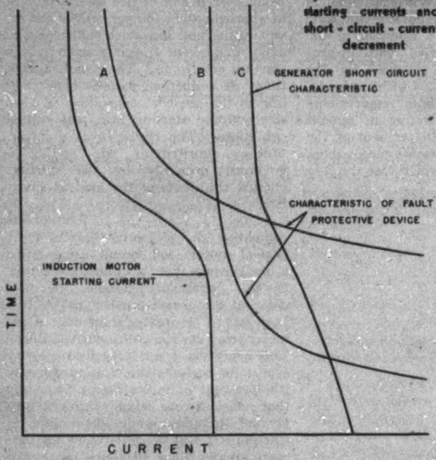


Figure 8 (left). Inverse-time overcurrent protection as affected by induction-motor starting currents and short-circuit-current decrement

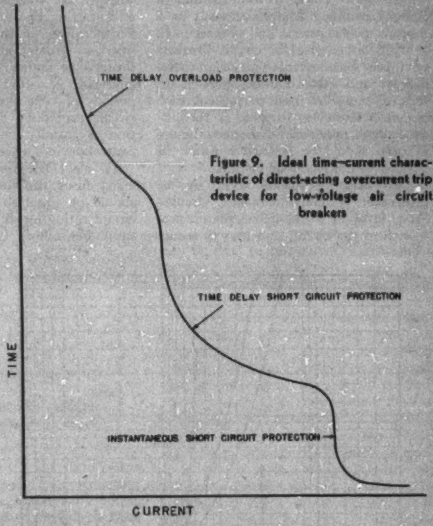
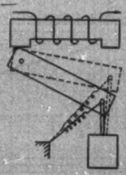


Figure 9. Ideal time-current characteristic of direct-acting overcurrent trip device for low-voltage air circuit breakers

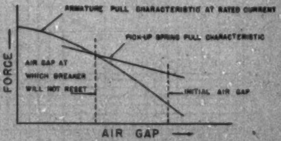
To summarize the foregoing discussion, it has been demonstrated that the conventional methods of securing selective operation in a circuit protective system have many shortcomings insofar as their application on shipboard is concerned. Current-differential protection offers little, if any, possibilities for shipboard use while directional overcurrent protection must be limited to generator circuits. On the other hand, inverse-time-delay overcurrent protection offers the best opportunity for securing the desired results, but the limitations of the various devices generally used for this purpose are such that they either are not sufficiently reliable or must be restricted to definite parts of the system. Finally, it has been shown that a direct-acting inverse-time-delay overcurrent trip device for air circuit breakers capable of giving accurate and consistent time delays of the type shown in Figure 9 provides the most satisfactory solution to the problem of circuit protection on shipboard. Such a device fulfills the combined requirements of (1) maximum circuit protection, (2) high-speed fault clearing, and (3) selective operation in a manner necessitating no external tripping source or control wiring, and therefore represents the maximum in simplicity and reliability.

Application of Circuit Protective Devices

In the application of circuit protective devices it is convenient to consider the



A. Change in air gap during introduction of time delay



B. Effect of change in air gap on reset

Figure 10. Change in air gap of direct-acting overcurrent trip device during introduction of time delay, and its effect on tripping characteristic

problem with regard to each of the basic types of circuits which occur on shipboard power systems. These circuits may be classified as follows:

1. Individual load circuits.
2. Feeder circuits.
3. Bus-tie circuits.
4. Individual generator circuits.

Individual Load-Circuit Protection

Individual load circuits are those which supply power to a single unit of power-consuming equipment such as a motor, transformer, and heater. In general, either fuses or circuit breakers equipped with inverse-time-delay overcurrent and instantaneous short-circuit tripping may be used to protect such circuits. However, in view of the disadvantages previously discussed, the use of fuses should be restricted insofar as possible to nonvital load circuits.

Two major problems occur in the protection of individual load circuits. First, in order to secure maximum protection of the circuit and also to secure selective operation with other circuit protective devices in the system it is essential that the circuit breaker or fuse operate to clear faults over a wide range of fault impedance and in minimum time. High-speed operation is particularly important from the standpoint of preventing damage to motor overload relays if they are of the thermal type. Second, the inverse-time overcurrent tripping characteristic of the circuit protective device must be co-ordinated with that of the overload protective device and starting characteristics of the load. Furthermore, it should be noted that overload protection of circuits, that is, protection against overheating due to overloads of small magnitude for long periods of time is not essential in the circuit protective device. Such protection is usually best provided by the control devices associated with the load.

To meet the foregoing requirements using fuses the fuse rating in general should be equal to 125 per cent of the circuit rating for all circuits except those supplying motors, in which case the fuse rating should be approximately two thirds of the locked-rotor current. However, in no case should the fuse size exceed 200 amperes.

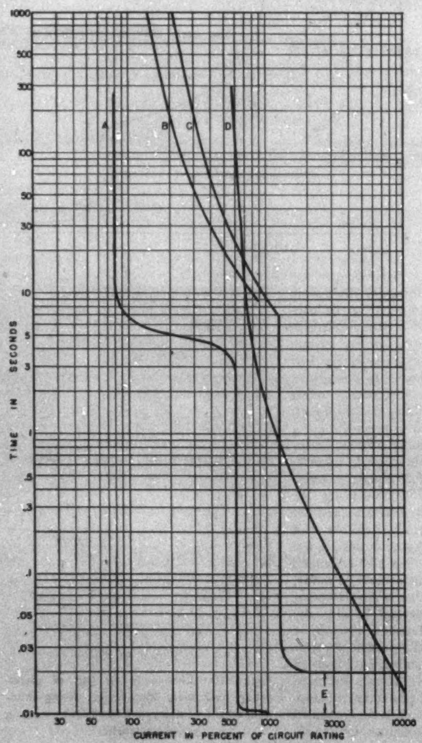


Figure 11. Co-ordination of protective devices on a single-motor-load circuit

Curve *A*—Induction-motor starting current

Curve *B*—Motor overload-relay time-current characteristic

Curve *C*—Circuit-breaker time-current characteristic

Curve *D*—Fuse time-current characteristic

Curve *E*—Circuit-breaker operating time

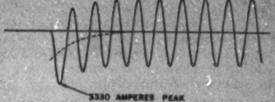


Figure 12. Typical transient current inrush on across-the-line start of 275-horsepower 440-volt induction motor

For circuit breakers, the inverse-time-delay overcurrent device should pick up at approximately 150 per cent of the circuit rating and introduce sufficient time delay to prevent tripping under motor-starting conditions. The usual starting times of induction motors vary from one to ten seconds, depending on their size and the starting load and voltage conditions. The circuit-breaker instantaneous short-circuit trip setting is governed primarily by the starting-current transients of the load as shown later.

Figure 11 shows the method of co-ordinating the protective devices for a typical motor load circuit using either a breaker or a fuse for circuit protection. In this particular example, the time delay of the circuit breaker and/or fuse is shown to be greater than that of the motor relay for the entire current range. This condition is not actually necessary except for currents below approximately 200 per cent of motor rating providing that sufficient time delay is introduced to prevent tripping while the motor is starting. It will be noted that for currents greater than 80 times the motor rating the fuse is faster than the circuit breaker, whereas for lower values of fault current the instantaneous trip feature on the circuit breaker enables it to clear faults in less time than is required by the fuse.

The chief limitation on the setting of the instantaneous trip device on the circuit breaker supplying a motor or transformer is the transient inrush current. When an induction motor is first energized a transient current flows which exceeds that determined by considering only the rms value of the applied voltage and the locked-rotor impedance of the motor. This transient current is similar to that which occurs when any highly inductive circuit is energized and is dependent on the point on the voltage wave at which the switch is closed and also on the degree of saturation in the magnetic circuit of the motor. Figure 12, taken from an oscillogram, shows the typical transient current which occurs when starting an induction motor across the line.

As shown in the appendix, the total transient motor-starting current may be resolved into an a-c component and a

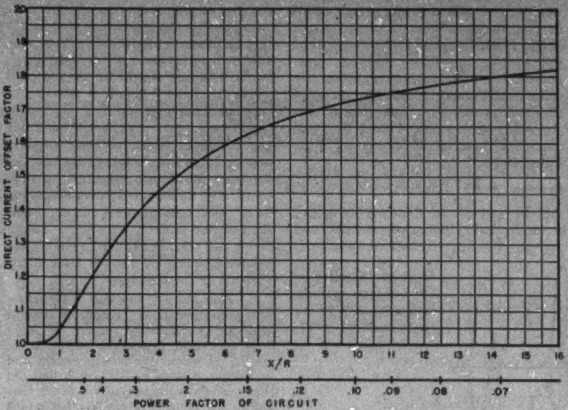


Figure 13. D-c offset factor as a function of circuit reactance-to-resistance ratio

d-c component. If saturation is neglected, the former is a constant with an rms value equal to the locked-rotor current. The latter is an exponential with an initial value dependent on the point on the voltage wave at which the switch is closed and decaying with a time constant determined by the ratio of the locked-rotor reactance to locked-rotor resistance.

Small air circuit breakers of the type commonly used on single-motor circuits for short-circuit protection are equipped with an instantaneous magnetic trip capable of being actuated by a half cycle or current. Moreover such circuit breakers are calibrated by means of a symmetrical alternating current having a specified rms value. In order therefore to prevent false tripping by the motor-starting transient it is necessary that the peak value of the symmetrical current by which the breaker is calibrated exceed the peak value of the starting transient. From the appendix, the peak value of the maximum transient occurs at one-half cycle after the switch is closed and is given by

$$i_{\text{peak}} = \sqrt{2}\, I_{LR}(1+e^{-\pi R/X}) \qquad (1)$$

where

I_{LR} = steady-state locked-rotor current in rms amperes
R = locked-rotor resistance in ohms
X = locked-rotor reactance in ohms

The value of the expression $(1 + e^{-\pi R/X})$ is known as the d-c offset factor and is plotted in Figure 13 as a function of X/R and the motor locked-rotor power factor. The rms value of the symmetrical current at which the circuit-breaker magnetic trip should be calibrated may be determined by multiplying the motor locked-rotor current by this factor. In many cases it is desirable to include the effect of the supply-circuit impedance in determining the value of the transient. This can be done by adding it to the locked-rotor impedance of the motor and using the new values of X and R in equation 1.

Because of saturation the actual peak value of the transient current in the first half cycle is greater than that obtained by the preceding calculations. Tests have shown that where the calculated peak value may be only 1.4 times the peak value of the locked-rotor current the actual measured value was 1.8. However, for typical induction-motor-starting power factors with some impedance in the supply circuit a d-c offset factor of 1.6 has been found satisfactory, particularly in view of the unlikelihood of closing in at exactly the point on the voltage wave giving the maximum transient.

For reduced-voltage starting with closed transition the factor of 1.6 also may be used to determine the maximum inrush current after first taking into account the reduction in starting current taken from the line.

Where reduced-voltage motor-starting equipment is used having open transition from the reduced-voltage tap to full voltage, a transient current greater than that which occurs with across-the-line starting may flow. As shown in the appendix, this transient current is caused by the out-of-phase relationship which may exist between the counter electromotive force of the motor and the line voltage at the time of reconnection. The existence of the motor counter electromotive force may be explained briefly as follows. The initial voltage applied to the stator of the motor produces a rotating

Figure 14. Oscillogram of transient inrush and counter electromotive force of 150-horsepower 440-volt three-phase induction motor during reduced-voltage starting with open transition

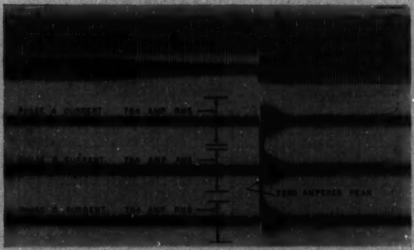
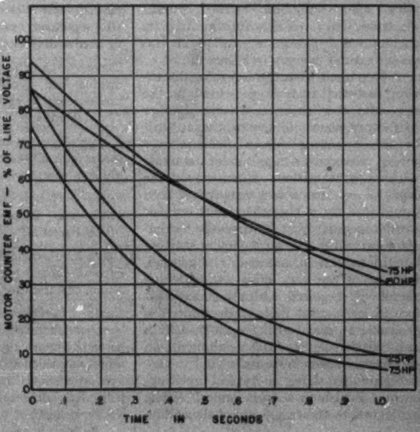


Figure 15 (right). Counter-electromotive-force-versus-time curves for open-circuited induction motors of various horsepower ratings



magnetic field which cannot disappear instantly. Consequently, at the instant after the circuit to the motor is opened, the rotating field remains locked in step with the rotor and generates a voltage in the stator, the magnitude of which is dependent on the initial applied voltage and the amount of current drawn by the motor prior to opening the circuit. The phase relation of this voltage with respect to the line voltage is determined by the original amount of slip, the time required to transfer, and the deceleration of the rotor which occurs during the transition period. The counter electromotive force is produced by current induced in the rotor and therefore decays exponentially with a time constant equal to the ratio of the inductance to the resistance of the rotor circuit.

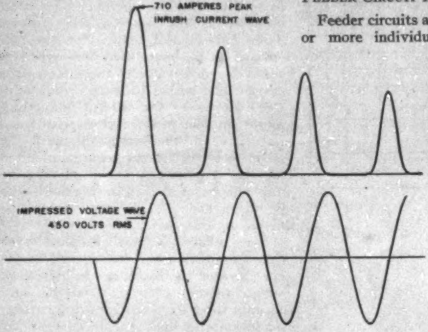


Figure 16. Oscillogram of transient exciting-current inrush of 25-kva single-phase 450/117-volt transformer

Figure 14 taken from an oscillogram shows the decay in counter electromotive force and the transient inrush current obtained on a 150-horsepower induction motor during reduced-voltage starting with open transition. The counter electromotive force as a function of time for unloaded motors of various horsepower ratings is given in Figure 15.

It is evident from the foregoing discussion and the analysis presented in the appendix that there is a large number of factors which determine the amount of the transient current which flows under reduced-voltage motor starting with open transition. However, on the basis of experience and tests it has been found that by setting the circuit breaker instantaneous trip at 1.8 times the locked-rotor current false tripping under starting conditions can be avoided. In general, because of the very high setting which thereby is required and the consequent loss in high-speed fault protection, it is desirable to avoid using open-transition reduced-voltage starting equipment on shipboard power systems.

Figure 16 shows a typical exciting-current inrush to a transformer. While analogous to the transients of the induction motor it differs in one major respect in that the saturation of the magnetic circuit is the dominant factor rather than the exciting impedance. For a three-phase-delta bank an instantaneous trip setting of 12 times the full-load rating of the bank is recommended. Where fuses are used, their rating should be equal to not less than the full-load rating of the transformers.

FEEDER CIRCUIT PROTECTION

Feeder circuits are those supplying two or more individual load circuits and usually originate directly at the main switchboard. Such circuits introduce the first major step in the problem of securing selective operation and for this reason circuit breakers should be used in preference to fuses. Figure 9 shows the tripping characteristics of a circuit breaker equipped with the direct-acting tripping device which has been described previously as incorporating the features necessary for circuit protection on shipboard, that is, time-delay overload, time-delay short circuit, and instantaneous short-circuit protective features. In applying this breaker to a feeder circuit, the following principles may be used in the selection of the settings:

1. *Time-Delay Overload Protection.* This protection should be directed towards achieving a maximum degree of high-impedance fault protection on the feeder circuit. However, the pickup current and time-delay settings must be such that the time–current characteristic lies above that due to any motor-starting or other inrush currents to the loads. In general, a pickup current setting equal to 150 per cent of the circuit rating with a time delay of 15–20 seconds at 250 per cent of circuit rating is satisfactory. In any event, the setting must be above that of the inverse time–current protective device on the largest individual load circuit being supplied.

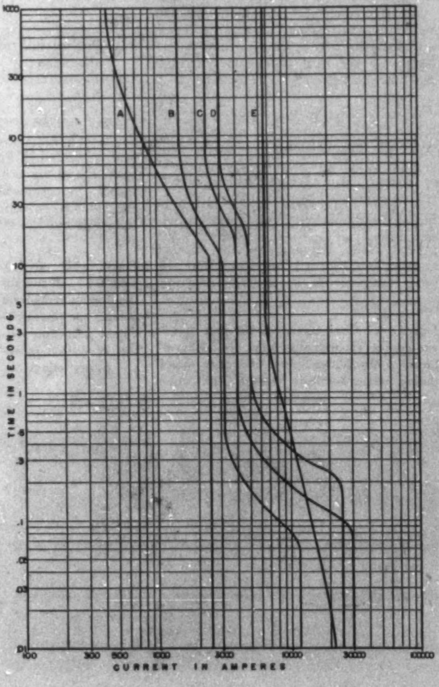


Figure 17. Typical co-ordination chart for fault protection on a shipboard a-c power-distribution system showing tripping characteristics of all significant protective devices

Curve *A*—Largest individual-load circuit-breaker tripping characteristic

Curve *B*—Largest feeder circuit-breaker tripping characteristic

Curve *C*—Bus-tie circuit-breaker tripping characteristic

Curve *D*—Generator circuit-breaker tripping characteristic

Curve *E*—Effective generator short-circuit current versus time with automatic voltage regulation, hot field, fully displaced current

2. *Time-Delay Short-Circuit Protection.* This protection is directed towards the high-speed clearing of low-impedance faults on the feeder circuit and at the same time selective operation with the individual load breakers or fuses. To do so it is necessary that the time delay of this protective device on the feeder breaker exceed the interrupting time of the largest load breaker for any value of current in excess of the pickup current setting. In general this time delay should not be less than three cycles. In addition, the pickup current value should exceed if possible the highest instantaneous trip setting of any load breaker. However, this is not imperative since fault currents in this range are not likely to occur.

3. *Instantaneous Short-Circuit Protection.* This protection is required wherever the short-time current rating of the feeder circuit breaker may be exceeded and also for backup protection of the load breakers or fuses if the maximum available short-circuit current exceeds the interrupting rating of the latter. Tests have shown that the instantaneous trip setting should be not more than 90 per cent of the interrupting rating of the breaker to be protected.

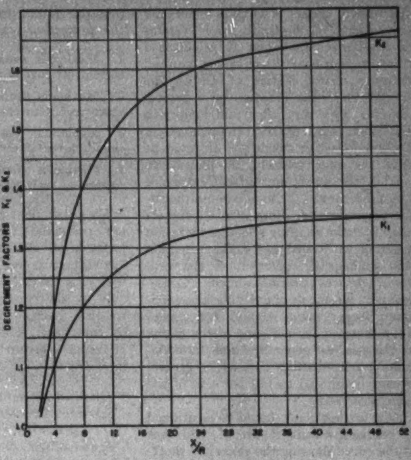


Figure 18. Factors for converting rms value of symmetrical current to maximum rms asymmetrical and average rms current in three phases

K_1—Ratio of average rms current in three phases at one-half cycle to rms value of symmetrical current

K_2—Ratio of maximum rms asymmetrical current at one-half cycle to rms value of symmetrical current

Bus-Tie Circuit Protection

Bus-tie circuits are those used to interconnect the main switchboards. They present a problem similar to that of the feeder circuits except that in addition it is usually necessary to co-ordinate the settings with both the feeder and the generator circuit breakers. For example, the pickup current setting of the time-delay short-circuit protection should not exceed approximately 80 per cent of the pickup current setting of the same device on the generator circuit breaker. Furthermore, the time-delay setting should be such that, for any value of current in excess of pickup, the time delay of the bus-tie circuit breaker exceeds the sum of the time delay and interrupting time of any feeder breaker. The instantaneous trip device should be set at the value determined by the short-time rating of the bus-tie breaker.

Generator Circuit Protection

Generator circuit protection consists of three major parts;

1. High-speed clearing of bus faults combined with selective operation with the bus tie and feeder circuit breakers.

2. Clearing of faults in the generator and generator cables.

3. Prevention of damage to the prime mover where two or more generators may be operated in parallel.

In order to accomplish the first objective it is necessary that the generator circuit breaker be equipped with inverse-time-delay overcurrent protection which can be co-ordinated with that of the bus tie and feeder circuits. The direct-acting overcurrent trip device previously described for use on the latter circuits also can be used for this purpose. However, an overcurrent relay having the proper characteristics also can be used in this connection, since little complication is introduced by the requirement for a source of tripping energy. The latter can be obtained conveniently and reliably from the generator exciter. The setting of the overcurrent device, whether a relay or a series overcurrent coil and time-delay device integral with the breaker, is governed entirely by the short-circuit current characteristic of the generator. Thus the pickup current setting of the time-delay short-circuit protective portion of the characteristic should not exceed approximately 80 per cent of the sustained short-circuit current of the generator, taking into account the effect of the voltage regulator. The purpose of this setting is to ensure maximum speed in clearing bus faults. The time-delay setting should be such that for any value of current in excess of pickup the time delay of the generator circuit breaker exceeds the sum of the time delay and the interrupting time of the bus tie and/or feeder circuit breakers. Where more than two generators may be operated in parallel it is desirable also to provide an instantaneous trip on the generator circuit breaker in the event that its short-time current rating may be exceeded and also to assist in clearing heavy faults in the generator or generator cables. Where used, this setting should be not less than 120 per cent of the maximum asymmetrical rms short-circuit current of the generator.

To prevent damage to the prime mover in installations where two or more generators are to operate in parallel, a reverse-power relay should be used and the circuit breaker tripped by means of an a-c shunt trip coil energized from the main bus. This type of trip coil is recommended in order that tripping also may be secured in the event of loss of excitation. The principal problem in connection with applying the reverse-power relay is to secure a setting sufficiently low to protect the prime mover but such that synchronizing power transients will not cause false operation. A pickup setting equal to four per cent reverse power and a time delay of approximately ten seconds at 125 per cent of pickup is usually satisfactory for this purpose.

It is evident that where all generators are of equal rating no selective operation between the generator circuit breakers for faults in the generator or generator cables can be secured by means of the inverse-time overcurrent trip devices. For this reason it often may be desirable to use a reverse-current relay to accomplish such protection on the generator circuit. However, this means another relay in addition to the reverse-power relay and, in some cases, the overcurrent relay, and represents therefore considerable complication and excessive space in the installation. Since the problem is similar to that occurring in a-c secondary-network systems, it would seem that a three-phase relay similar to the network master relay could be used for this purpose. The latter relay is capable of tripping both on reverse power and under fault conditions on the primary feeder supplying the network. Thus, it could be used for both reverse power and fault protection on generator circuits on shipboard. Although such an application has not as yet been made, it may be worthy of consideration in those cases where both reverse power and fault protection is desired on the generator circuit.

The foregoing discussion of the methods of securing circuit protection in each of the several classes of circuits commonly encountered on shipboard power systems is summarized in Figure 17, which is a co-ordination chart showing the required tripping characteristics of all of the significant circuit breakers on a typical system. Two very important factors in the development of the protective system to give selective operation are evident from this chart, namely, the limitations on (1) the maximum current rating of any feeder and (2) the maximum motor-starting currents. Both of these factors tend to make securing selective operation together with maximum fault protection and high-speed operation of the protective devices extremely difficult. Consequently, insofar as practicable, the maximum feeder rating should not exceed approximately 50 per cent of the rating of the smallest generator, and the maximum motor-starting current in general should be limited to 100 per cent of the generator rating.

One further point should be mentioned in connection with Figure 17, and that is the curve showing the generator short-circuit current. It will be noted that the integrated value of this current has been plotted as a function of time rather than the instantaneous value. Both relays and direct-acting trip devices are responsive to the total energy input. Consequently, by plotting the integrated value of the generator short-circuit current and noting its intersection with the characteristic curve of the protective device, it is possible to predict the time required to clear a fault; such information is essential to the development of a co-ordinated protective system.

Fault-Current Calculations

An essential prerequisite to the development of a circuit protective system and the application of the various protective devices is the determination of the system short-circuit currents. The assumption that on low-voltage systems these currents can be considered inherently limited to relatively low values has been found by experience to be unjustified, particularly on large-capacity shipboard power systems where the cable sizes are large and their lengths short. Moreover, in calculating the fault currents on shipboard low-voltage power-distribution systems it is necessary to determine not only the contribution of the synchronous generators and motors but also that of the induction motors which are connected to the system. For, although the short-circuit currents from the latter decay very rapidly, the high-speed operation and limited thermal capacity of low-voltage air circuit breakers makes this component of the current an important factor in determining the interrupting duty on both breakers and fuses.

It is necessary to calculate at least the following fault currents in the design of the protective system:

1. Maximum available rms short-circuit current (average of three phases).
2. Maximum available rms asymmetrical short-circuit current.
3. Maximum rms asymmetrical short-circuit current of each generator.
4. Maximum sustained rms short-circuit current of each generator.

* The subsequent discussion and formulas are presented for convenience. The methods of calculating fault currents on low-voltage power systems are already well established,[1,2,3] and it is intended only to show in detail their application to the specific problems encountered on shipboard. The following definitions of terms apply to each of the formulas in which they appear:

I_l = rated generator current in amperes
E_g = rated line-to-neutral generator voltage in volts
I_g = sustained short-circuit current of generator
I_{avg} = maximum available rms short-circuit current (average of three phases)
I_{max} = maximum available rms asymmetrical short-circuit current
I_g'' = maximum rms asymmetrical short-circuit current of generators
Z_d'' = equivalent system impedance in ohms per phase from generators to point of fault using subtransient reactances of generators
Z_d = equivalent system impedance in ohms per phase from generator to point of fault using synchronous reactances of generators
X_d = generator synchronous reactance in ohms per phase
I_{fsc} = generator field current under sustained short-circuit conditions
I_{fn} = generator field current at no-load rated voltage
Z_m = equivalent system impedance in ohms per phase from connected induction motors to point of fault using locked-rotor impedance of motors
K_1 = ratio of average three-phase rms current to rms symmetrical current from Figure 18
K_2 = ratio of maximum rms asymmetrical to rms symmetrical current Figure 18

MAXIMUM AVAILABLE RMS SHORT-CIRCUIT CURRENT (AVERAGE OF THREE PHASES)

For convenience in testing and application the interrupting rating of all low-voltage (600 volts or less) air circuit breakers is based on the average value of the maximum available rms short-circuit currents which occur in the three phases during the first cycle after inception of the short circuit. In making the calculation, the current should be determined on the basis of the contribution of the maximum expected paralleled generating capacity plus the contribution of the induction- and synchronous-motor load connected to the system at the time of fault. The impedance should include the generator and motor subtransient reactances, armature resistances, and all significant circuit impedances (cables, circuit breakers, and bus work) up to the point of fault. The fault should be considered to be a zero-impedance three-phase fault. All generators should be assumed to be operating at rated load and power factor prior to the short circuit. The fault current is given by

$$I_{avg}=\frac{0.70E_g}{Z_m}+\frac{K_1E_g}{Z_d''} \tag{2}$$

MAXIMUM AVAILABLE RMS ASYMMETRICAL SHORT-CIRCUIT CURRENT

This current is required in connection with the short-time current rating and mechanical strength of switches, cables, bus work, and circuit breakers, and has recently been proposed as the basis for the interrupting rating of fuses. This current should be calculated on the same basis as that described previously in connection with the average current. The asymmetrical short circuit-current may be determined as follows:

$$I_{max}=\frac{0.80E_g}{Z_m}+\frac{K_2E_g}{Z_d''} \tag{3}$$

MAXIMUM ASYMMETRICAL SHORT-CIRCUIT CURRENT OF EACH GENERATOR

This current value is needed in connection with prescribing the current range over which the inverse time-current characteristics of the overcurrent protective devices on the generator, bus tie, and feeder circuit breakers must be co-ordinated in order to maintain selective operation. It is also required with regard to the pickup setting of the instantaneous short-circuit trip on the generator circuit breaker. The current should be determined on the basis of the generator being short-circuited when operating at rated load and rated power factor. Hence it is obtained from

$$I_g''=\frac{K_2E_g}{Z_d''} \tag{4}$$

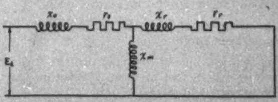


Figure 19. Equivalent circuit of induction motor under across-the-line starting conditions

r_s—Stator resistance
x_s—Stator leakage reactance at rated frequency
r_r—Rotor resistance
x_r—Rotor leakage reactance at rated frequency
x_m—Shunt reactance to include magnetizing current
E—Applied voltage
I_s—Stator current

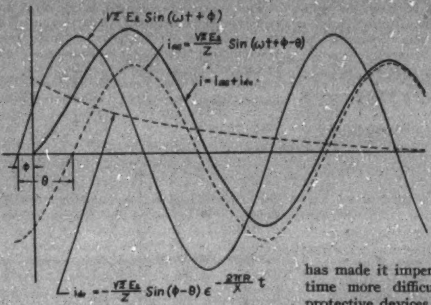


Figure 20. Transient current inrush for an RL series circuit

MAXIMUM SUSTAINED SHORT-CIRCUIT CURRENT OF EACH GENERATOR

Because this current governs the pick-up current settings of the time-delay short-circuit trip devices on the generator and indirectly on the bus-tie and feeder circuit breakers, it is of major importance in the design of the circuit protective system from the standpoint of selective operation and fault-clearing time. The calculation should include the effect of the voltage regulator using the value of the field resistance with the field hot. Hence this current is given by

$$I_g = \frac{\left(\frac{I_{fsc}}{I_{fo}}\right)(SCR)(X_d)(I_l)}{Z_d} \qquad (5)$$

In equations 2 and 3 of the foregoing calculations the factors of 0.7 and 0.8, used in determining the contribution of induction motors to the average and asymmetrical values, respectively, of the available short-circuit current, are necessary to take into account the decrements in the a-c and d-c components of the motor contribution during the first half cycle. They are used in order to avoid making a calculation for each motor individually. Accordingly, the factors were derived on the basis of reducing all of the connected induction motors to a single equivalent motor having a rating equal to the sum of the ratings of all the connected motors. This equivalent motor was assumed to have a short-circuit time constant of two cycles and an open-circuit time constant of 30 cycles. From Figure 15 it will be noted that the latter corresponds to the time constant of motors of approximately 25 to 50 horsepower which is about the average motor size usually encountered on shipboard.

Conclusions

The growth in the capacity of the electric plants installed on shipboard has created a need for more careful analysis of the fault currents and the methods of obtaining fault protection. Moreover, it has made it imperative and at the same time more difficult to provide circuit protective devices of adequate interrupting capacity and capable of selective operation under fault conditions.

The use of conventional relaying schemes for achieving selective operation on shipboard possesses a number of disadvantages from the standpoint of space, complexity, and reliability. On the other hand, circuit breakers equipped with direct-acting tripping devices utilizing the inverse-time–current principle and capable of giving accurate time delays as described herein are believed to present the best method of securing selective operation on shipboard power systems.

It is considered that the application of circuit protective devices in accordance with the principles and methods presented herein will result in a co-ordinated protective system possessing the following features which are essential to meeting the requirements of marine service:

1. Adequate interrupting capacity.
2. High-speed fault clearing.
3. Selective operation.
4. Maximum protection.
5. Maximum simplicity and ease of maintenance.

Appendix. Analysis of Induction-Motor Switching Transients

Across-the-Line Starting Transient

The conventional equivalent circuit of an induction motor under starting conditions, rotor at rest, slip = 1, is shown in Figure 19. The circuit is similar to that of a resistance and inductance in series and, assuming no saturation, the transient current then is given by the well-known formula:

$$i = \frac{\sqrt{2}E_l}{Z}\left[\sin(wt+\phi-\theta) - e^{-2\pi Rt/X}\sin(\phi-\theta)\right] \qquad (6)$$

where

$\theta = \tan^{-1}\frac{X}{R}$ = power-factor angle of locked-rotor current

ϕ = angle on voltage wave at which switch is closed

$Z = \sqrt{(r_s+r_r)^2+(x_s+x_r)^2}$ = locked-rotor impedance in ohms

$R = r_s+r_r$ = locked-rotor resistance in ohms

$X = x_s+x_r$ = locked-rotor reactance in ohms

t = time in cycles after closing circuit

E_l = rms value of line-to-neutral voltage

Equation 6 is plotted in Figure 20, and as shown therein the total transient current can be resolved into an a-c component and a d-c component as follows:

$$i_{ac} = \frac{\sqrt{2}E_l}{Z}\sin(wt+\phi-\theta) \qquad (7)$$

$$i_{dc} = \frac{\sqrt{2}E_l}{Z}\sin(\phi-\theta)e^{-2\pi Rt/X} \qquad (8)$$

From Figure 20 it is evident that the maximum value of the transient occurs when the d-c component is a maximum or, from equation 8, when the switch is closed at a point on the voltage wave such that $\phi-\theta=90$ degrees, 270 degrees, and so forth. Hence, if we let $\phi-\theta=90$ degrees,

$$i_{max} = \frac{\sqrt{2}E_l}{Z}\sin(wt+90°) - \frac{\sqrt{2}E_l}{Z}e^{-2\pi Rt/X}$$

$$= \frac{\sqrt{2}E_l}{Z}\cos wt - \frac{\sqrt{2}E_l}{Z}e^{-2\pi Rt/X} \qquad (9)$$

The peak value of the current wave occurs with $\cos wt = -1$ or $t = {}^1/_2$ cycle. Hence,

$$i_{peak} = -\frac{\sqrt{2}E_l}{Z}(1+e^{-\pi R/X}) \qquad (10)$$

or

$$i_{peak} = -\sqrt{2}\,I_{LR}(1+e^{-\pi R/X}) \qquad (11)$$

where

$I_{LR} = \frac{E_l}{Z}$ = steady-state locked-rotor current in rms amperes

Equation 11 is given as equation 1 in the text.

Reduced-Voltage Starting With Open Transition

The equivalent circuit of an induction motor connected to a reduced-voltage starter of the open-transition autotransformer type is shown in Figure 21. The initial starting transient in terms of the line current is obtained by substituting the voltage aE_l for E_l in equation 10, and multiplying by the transformation ratio, a. Thus,

$$i_{peak} = \frac{a^2\sqrt{2}E_l}{Z}(1+e^{-\pi R/X})$$

$$= a^2\sqrt{2}\,I_{LR}(1+e^{-\pi R/X}) \qquad (12)$$

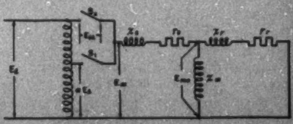


Figure 21. Equivalent circuit of induction motor under reduced-voltage starting with open transition

Following starting, the steady-state condition existing with the motor connected to the reduced-voltage tap (switch s_1 closed) is expressed by

$$aE_l - I_s(r_s + jx_s) = E_{m0} \tag{13}$$

Then if it is assumed that the motor is started under no load and is brought up to very nearly synchronous speed on the reduced-voltage tap, the stator voltage drop becomes negligible and equation 9 reduces to

$$aE_l = E_{m0} \tag{14}$$

The voltage E_{m0} is a counter electromotive force which is generated in the stator under steady-state conditions by the resultant rotating field produced by currents I_s and I_r. When switch s_1 is opened the flux linkages with the rotor produced by this field cannot collapse immediately but are sustained at their initial value by a current induced in the rotor. Thus the field continues to rotate locked in step with the rotor. The induced current in the rotor decays exponentially and, neglecting the small change in speed, the rotating field produced thereby generates a voltage in the stator during the transition period given by

$$\begin{aligned} E_m &= E_{m0}e^{-t/T_{d0}'} \\ &= aE_le^{-t/T_{d0}'} \end{aligned} \tag{15}$$

where

E_m = motor terminal voltage at any instant following opening the line switch

$T_{d0}' = \frac{x_m + x_r}{2\pi r_r}$ = the open-circuit transient time constant of the rotor circuit in cycles

t = time in cycles after opening switch

At the instant switch s_1 is opened the countervoltage, E_m, is very nearly 180 degrees out of phase with the line voltage, E_l. However, the initial slip and the deceleration of the rotor during the transition period cause the motor voltage to change in phase relation with respect to the line voltage. Hence by taking into account the decrease in magnitude given by equation 15 and the change in phase position, the motor voltage during the interval when switch s_1 is open can be plotted in polar form as shown in Figure 22. The voltage difference between E_m and E then gives the voltage, E_{s2}, which exists across the switch s_2 on the full-voltage tap.

With the voltage E_{s2} determined in the foregoing manner, the peak transient current at one-half cycle is given by substituting E_{s2} in equation 10 and changing the impedance and time constants as follows:

$$i_{\text{peak}} = \frac{\sqrt{2}\,E_{s2}}{Z'}(1 + e^{-\pi R'/X}) \tag{16}$$

where

$$Z' = \sqrt{r_s^2 + (x_s + x_r)^2}$$

$$R' = r_s$$

$$X = x_s + x_r$$

since

$$Z' \cong Z \text{ and } I_{LR} = \frac{E_l}{Z}$$

then

$$i_{\text{peak}} = \sqrt{2}\,I_{LR}\frac{E_{s2}}{E_l}(1 + e^{-\pi R'/X}) \tag{17}$$

It is evident from Figure 22 that the switch voltage, E_{s2}, may have a large number of values depending on

1. The initial voltage tap.
2. The time of transfer.
3. The initial motor slip.
4. The amount of load or decelerating torque on the motor.

In addition, whether or not the maximum transient occurs depends on the point on the voltage wave at which the switch, s_2, is closed. Thus, by using an 80 per cent voltage tap and assuming the motor voltage to have remained constant in magnitude but to have shifted 180 degrees in phase angle, it would be possible, theoretically, for the transient peak to equal 1.8 times the transient peak with across-the-line starting.

It is difficult to establish the characteristics which an open transition starter should possess in order to minimize the transient, although it may be stated in general that, the shorter the transition time and the higher the voltage tap used, the smaller the transient current which will occur on transfer to full voltage.

For the usual controller with a transition time of five to eight cycles and the normal conditions of starting under no load or light load, it is practicable to assume the switch voltage to be not greater than full line voltage. Then equation 17 can be modified as follows:

$$\begin{aligned} i_{\text{peak}} &= \sqrt{2}\,I_{LR}\frac{E_l}{E_l}(1 + e^{-\pi R'/X}) \\ &= \sqrt{2}\,I_{LR}(1 + e^{-\pi R'/X}) \end{aligned} \tag{18}$$

The foregoing equation is identical to equation 11 for across-the-line starting with the exception of the term in parenthesis wherein only the stator resistance is used in computing the damping factor of the d-c component. For the typical induction motor, where $x_s = 0.08$, $x_r = 0.08$, $r_s = 0.02$, and $r_r = 0.02$ in per unit values, this becomes, from Figure 13, equal to 1.6 instead of 1.4. Again saturation makes the actual transient current higher than that computed from equation 17; hence a factor of 1.8 times the value of the locked-rotor current is recommended for determining the calibration point of the circuit breaker.

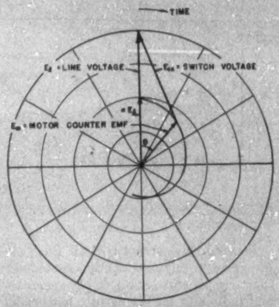


Figure 22. Relation between line voltage, motor voltage, and switch voltage for induction-motor reduced-voltage starting with open transition

θ = Phase angle between line voltage and motor voltage

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Cyclic Movement of Cable—Its Causes and Effects on Cable-Sheath Life

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Synopsis: Results of a field survey and laboratory tests are presented in this paper showing the relationships between loading, conductor-temperature changes, movement in manholes and ducts, and sheath life for paper and lead cable. The record on underground 15-kv cable is included to emphasize the noninherent failures attributable to physical proportions of underground structures. Improvements in modern cable have permitted operation at increasing temperatures with few inherent failures.

The tests and operating experience indicate that unsatisfactory sheath life caused by fatigue of lead can result from cyclic movement of cable although operating at temperatures allowed in standard cable specifications. A method is outlined for computing sheath strains for given cable offsets and movements at the duct mouth. Fairly reasonable estimates of sheath life can be prepared.

The tests indicate also that snaking of cable within the ducts may impose severe cyclic strains on the lead and become the determining factor for optimum sheath life.

Marked improvement in quality of impregnated-paper-insulated lead-covered cable has been accomplished in the past 25 years through excellent research and progress in the selection of component materials and the revision of production methods. The era of early development with its inherent failures caused by high-loss compounds, defective taping, sheath laminations, poor welds, and impurities trapped in the lead apparently has come to a close. This has been reflected in the small number of cable failures chargeable to inherent defects in insulation and sheath, which in turn has encouraged the continuing trend toward higher conductor-temperature limits and resulting increases in current-carrying capacities. Furthermore, emergency ratings have been adopted within the past ten years to provide for operation at elevated temperatures for short periods in event of cable or equipment failures. Consequently, increasing amounts of cable are being loaded normally at values more nearly approaching the limits based on the regular Association of Edison Illuminating Companies conductor temperatures.[1]

Studies of operating records such as shown in Figure 1 reveal the shift of emphasis from electrical performance of insulation to the mechanical performance of the sheath. Already, lead-sheath deterioration caused by cable movement is responsible for five times as many failures on 700 miles of 15-kv three-conductor belted cable as resulted from insulation deterioration. This ratio nevertheless increased to seven times for 22 miles of generating-station tie lines which were operated for system-economy reasons at maximum capacity over a 17-year period, during a year and a half of which the cables were subjected to heavy overloads.

The predominant cause of cable-movement failures involves cracks in the lead sheath. This accounted for 46.6 per cent of all failures on the station tie lines, or over twice the percentage on the entire system. These cracks could not be charged against the manufacturer as inherent defects, but resulted from the physical proportions of the underground structures in which they operated. Figure 2 is typical of these cracks as they occur along the sheath in the manhole at and near the cable-joint wipe. Cracks also occur in the sheath on the cable inside the duct. Figure 3 clearly shows the unstrained longitudinal area *C–C* along the axis of the cable while the areas *A–A* and *B–B* are severely fatigued opposite walls of the cable. The latter were subjected to the cyclic strains as the cable flexed with the typical snaking motion. Figure 4 is a photomicrograph showing the fatigue cracks in a section of the same sheath adjacent to the fault located near the center of a 200-foot run of the generating-station tie cables previously mentioned.

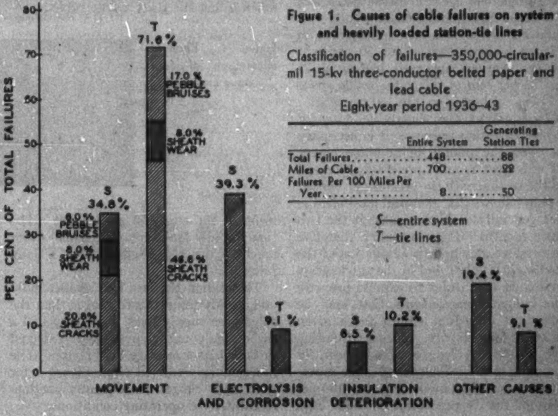

Figure 1. Causes of cable failures on system and heavily loaded station-tie lines

Classification of failures—350,000-circular-mil 15-kv three-conductor belted paper and lead cable
Eight-year period 1936–43

	Entire System	Generating Station Ties
Total Failures	448	88
Miles of Cable	700	22
Failures Per 100 Miles Per Year	8	50

S—entire system
T—tie lines

It was apparent that satisfactory operation of paper and lead cable depended not only on limiting conductor temperatures, but also on establishing maximum daily ranges of temperature. However, in order to establish such a rating policy on a sound basis, it was necessary to evaluate the many factors that determine the time to fracture of the lead sheath on an operating cable. The discussion in this paper outlines the results to date of the field survey and laboratory tests which were conducted to secure reliable data on the following relationships:

1. The amount of cable movement that occurs at the duct mouth with various daily ranges of conductor temperature.
2. The number of movement cycles the lead sheath can withstand before sheath cracks appear.
3. The nature and extent of cable snaking inside the duct.

Cable Movement in Manholes

About 350 measurements of longitudinal cable movement at the duct mouth were made using maximum-movement indicators similar to Figure 5. Records were made also on celluloid dials using

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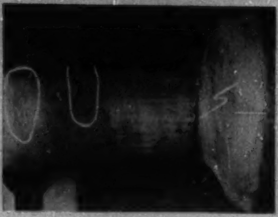


Figure 2. Cracks in lead sheath and at joint wipe on cable in manhole

graphic movement recorders. Cable lengths varied from 126 to 655 feet and involved a wide variety of loading on station tie lines, substation supply lines, and industrial feeders.

MOVEMENT VERSUS TEMPERATURE RANGE

The practical effect in the field of changes in conductor temperature is shown in Figure 6. There is approximately a linear relationship between cable movement and conductor-temperature range. The movement is the sum of the measurements at both ends of a cable length and was plotted this way for comparison with the movement data from the laboratory duct-run tests. The ranges in conductor temperature were secured from computed hour-to-hour temperature changes, based on hourly load readings and taking into account the time constant of similar cable, as determined from actual tests.

The curves were plotted using averages for a large number of sections. Divers field influences produced a spread of data which were evaluated in probability percentages. Thus, 300-foot sections of cable with a daily range of 20 degrees centigrade had 0.4 inch as the average of the total cable movements at both duct mouths. However, the subdiagram in Figure 6 shows that about 50 per cent of the sections surveyed had from 75 to 125 per cent of this value, while about 85 per cent had from 0 to 150 per cent.

Figure 3. Fatigue-crack areas *A–A* and *B–B* along longitudinal axis of cable on inside surface of lead sheath removed from middle of stretch

Area C–C is not fatigued

In applying the sheath-life data for design or rating purposes, it is necessary to know the amount of cable movement at one duct mouth, preferably the end of the section with the greater movement. Table I shows that the total movement is not generally divided equally at the two duct mouths. It is necessary therefore to produce a composite evaluation of the factors in Table I and in the subdiagram in Figure 6 in order to express properly the range of movements that may be found at one duct mouth for a given range of temperature. Such a tabulation for a 300-foot length would show for example that predetermined cable movement at one duct mouth equal to 100 per cent of the average total for both ends from the curve in Figure 6 produces a corresponding life or better for 86 per cent of the cases. The remaining 14 per cent would have movements from 100 to 200 per cent of the average total with shorter resulting life of sheath.

Figure 7 shows how the movement–temperature-range pattern may change from day to day on the same cable. The daily cycles of cable movement consequently vary from the weekly range. There appears to be no perceptible lag between cable movement and change in conductor temperature.

TEMPERATURE RANGE VERSUS LOADING

Table II demonstrates that it is inappropriate to use directly computed maximum conductor-temperature rises above the duct-bank temperatures in ascertaining the daily cyclic cable movement in the study of sheath life. The typical daily load factor determines how much of this rise is effective as a daily range.

Figure 4. Photomicrograph of fatigue cracks

The station tie lines had smaller daily and weekly temperature ranges than the substation supply lines, since they had a higher daily minimum load as reflected in their higher average load factor. The movements predicted from Figure 6 for such daily ranges consequently are representative of operating conditions.

Table I. Division of Total Duct-Mouth Movement

Per Cent of All Cases	Per Cent of Total Movement Appearing at One Duct Mouth
15.2	50
25.5	51–60
35.1	61–70
13.4	71–80
5.4	81–90
5.4	91–100
Total 100.0 (165 cable lengths)	

Cable-Sheath Life

The factors that influence the life of cable sheath were investigated by tests on 17 samples of three-conductor 350,000-circular mil 15-kv belted paper-insulated lead-covered cable in the dummy manhole, shown in Figure 8. Life was expressed as the number of cycles the cable could be moved, simulating expansion and contraction in service, until the typical circumferential lead-sheath fatigue cracks appeared. In every case the fracture developed near or at the edge of the joint wipe, although in several cases cracks occurred also at the bend in the offset or at the duct mouth. These cracks had exactly the same appearance as those found in service.

Table III contains the range of cable movements and widths of offsets tested. Life was expressed in years based on routine field load-survey data which showed that lines have about 275 cycles of normal loading per year. This allows for negligible cycles over weekends and holidays. All tests were conducted with the dummy manhole adjusted to ten feet four inches in length. This gave a 52-inch-long offset, or distance from the duct mouth to a line drawn at right angles to the cable at the joint wipe. This is the dimension L' in Figure 21 and Appendix *A*. Movement of the cables for predetermined amounts at the duct mouths was secured by cross heads operated from a motor mechanism through a pair of cams. The heating equipment served only to maintain the sheath at a sustained elevated temperature during each test run.

EVALUATION OF CABLE MOVEMENT IN TERMS OF UNIT STRAIN

The cyclically repeated strains in the lead sheaths were evaluated on the basis of the deflection formula for a cantilever beam, derived in Appendix *A*. This permitted a study of the effect of cable movement on sheath life by eliminating the dimensions of the offset and the magnitude of cable movement as independent variables.

The data in the first group of tests in Table III are shown in curve I in Figure 9 to comprise practically a first-power relationship between computed unit strains and sheath life with sheath at room temperature. When the cables in the second group were tested, with elevated sheath temperatures to correspond to operation in heavily loaded duct banks, there was a pronounced scatter in the results. These were reflected in Curve II. The remaining tests in Table III, each involving several variables, also are shown in Figure 9 as a matter of interest.

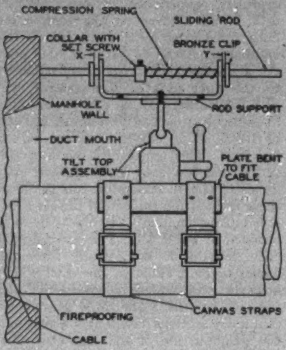


Figure 5. Maximum-movement indicator

Maximum longitudinal movement during observation interval equals sum of dimensions *x* and *y*

Figure 6. Longitudinal cable movement at both duct mouths versus conductor-temperature range and cable length based on field-survey data

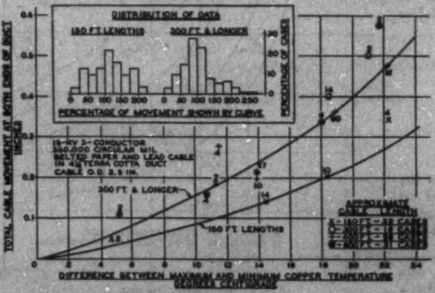


Table II. Variation of Conductor-Temperature Ranges With Load Factors

Type Supply Line	Conductor Temperature Ranges and Rises: Average Load Factor	Daily Range (Deg C)	Weekly Range (Deg C)	Ratio Daily/Weekly Ranges (Per Cent)	Computed Weekly Max Rise (Deg C)*	Ratio to Computed Weekly Max Rise (Per Cent): Daily Range	Weekly Range
Station tie	0.79	14.7	19.0	77.4	27.0	55	70
Substation	0.67	16.0	21.0	76.2	24.5	65	86
Substation	0.61	18.5	23.4	79.1	25.3	73	93
Industrial	0.62	12.0	17.3	69.4	21.7	55	80

* Total rise of conductor temperature above duct-bank temperature is 27 degrees centigrade if 6,000-kva peak is sufficiently sustained on 15-kv three-conductor 350,000-circular-mil belted paper and lead cable with outside diameter of 2.5 inches.

EFFECT OF LEAD-SHEATH TEMPERATURE

Curves I and II in Figure 9 show the influence of temperature on lead fatigue. The lives of cable sheaths operating at temperatures ranging from 35 to 42 degrees centigrade are about 70 per cent of the values at room temperature (18 to 25 degrees). Since sheath-fatigue fractures occur in the duct where lead temperatures can exceed 60 degrees centigrade, additional tests are necessary to explore this extended range. There is good reason to believe that such data will permit evaluation of a temperature coefficient to be applied in a strain-life formula and also will serve to check the drop in life indicated by sample 4*C*. Such a loss in ability to withstand strain would be consistent with the great decrease in time to burst at elevated temperature.[2]

CABLE MOVEMENT VERSUS SHEATH LIFE

The sheath-life-versus-movement data can be used more conveniently in manhole design and in cable rating in the form presented in Figure 10. These curves were constructed on the basis of the curve I in Figure 9. Figure 10 would be applicable where cables operate in lightly loaded duct banks. The test points were plotted in these figures to show a reasonable agreement with the constructed curves. Such curves are applicable only to a specific-size cable and length of offset. Reasonably long life is indicated for cable movements of about 0.2 inch in a manhole about ten feet long. Sheath life declines sharply with increase of movement to about 0.5 inch.

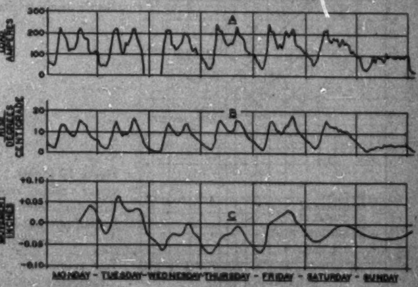
In order to check the test results against the operating record of the 22 miles of station tie cables, Figure 11 was prepared largely on the basis of the extrapolated portion of curve II in Figure 9 to show the life for various movements in shorter manholes existing on these lines. An extremely rapid drop in life results for movements up to 0.3 inch. The 27-inch-long offset is representative of a large number of manholes where a considerable number of sheath cracks were reported.

DISTRIBUTION OF STRAIN

Measurements of the unit strain per cycle were made at points three inches apart along the cable in the dummy manhole on six samples before fireproofing. This followed a method discussed in Appendix *B*. Figure 12 shows typical distributions of these strains for two widths of offset. Since the cables subsequently were fireproofed, there was no indication of the possible effect of the jacket of fireproofing in causing lower strains at the joint wipes and at the duct mouths as the tests progressed.

Table IV shows that the computed unit strains compare very favorably with the averages of the measured maximum values at the two joint wipes where the peak strains generally occur. The computed strains thus are seen to be adequately representative to serve as a basis

Figure 7. Comparison of cable movement (*C*) at one duct mouth with load (*A*) and computed conductor-temperature rise (*B*)



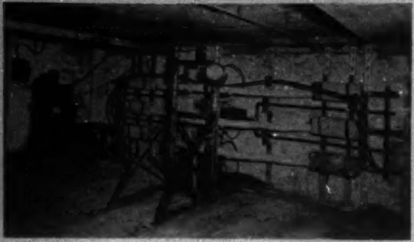


Figure 8. Dummy-manhole test apparatus

Joint-support rack in center foreground is moved along floor channel for large changes in width of offset

for evaluating the effect of cable movement and offset dimensions on sheath life.

Comparison With Results of Other Investigators

Curve III in Figure 9 shows the effect of cyclic strains (computed by the method in Appendix *A*) on time to fracture of cable sheath at room temperature based on test results recently reported by the Commonwealth Edison Company.[3] These data from their dummy-manhole tests are outlined in Table V and involve sheath lives about double those indicated in curve I. The difference can be explained in part on the basis of the period and nature of the cycle of strain. In the Chicago tests a continuous 70-second cycle was used, while the Philadelphia test apparatus had a four-minute cycle. The latter consisted of 45 seconds to move the cable at both duct mouths from one extreme position to the other, and a 75-second rest interval at each of these extreme positions. It is possible that changes took place in the structure or alignment of the lead crystals during the rest periods and the different rates of movement which were reflected in reduced sheath life equivalent to an increase in over-all cyclic strain.

This point of view is supported by the results of fracture tests recently publised by the University of Illinois.[4] Figure 13 is a partial reproduction of their Figure 21. The comparable Chicago and Philadelphia test results in terms of cyclic sheath strains, both as measured at the point of maximum strain and as computed, have been added. The Illinois values of strain apply to lead strips repeatedly bent equidistant in both directions from a straight neutral position. These strains were evaluated for the point of fracture at either extreme position in the deflection and therefore were only one half of the total strain for a complete cycle. It is extremely interesting to note that the ratios of the comparable permissible strains for 5,000-cycle life to those for 10,000-cycle life, were about the same in the Chicago and Philadelphia tests (on the computed basis) as in the original University of Illinois curves.

However, a daily load cycle (approximately 0.0007 cycle per minute) suggests that an over-all curve in Figure 13 must become asymptotic or be flatter originally as a result of rest periods. Otherwise, ridiculously low permissible strains would be indicated for actual operating conditions.

Life Indicated by Test Data Versus Failure Record

Computed values of sheath life were found to be in close agreement with operating experience. About 34 per cent of all failures on the station tie lines were caused by sheath cracks in the manholes. About 14 per cent were near the joint wipes, and 12 per cent adjacent to the duct mouth. Comparable system figures were 17, 8, and 6 per cent. Sheath cracks occurred on about 200 out of the 360 cable lengths in service on the tie lines. This reasonably confirms the survey and test data which support estimates of sheath cracks on 214 to 260 of these lengths for the actual offsets used. A complete summary of this analysis is outlined in Table VI. The listed actual offset lengths do not include the eight inches of bent cable in the duct which however were added in considering the effective lengths. The table also shows that many sections have long life although the weighted life of the line was computed at 15.7 to 20.1 years. Change in manhole length to ten feet indicated a weighted life of 27.3 years.

Cable Performance Within Ducts

Studies of cable movement in the duct were undertaken for two fundamental purposes. One was to determine the portion of the theoretical expansion that appeared at the duct mouths under controlled conditions. The other was to ascertain the nature and extent of snaking, together with the relative perform-

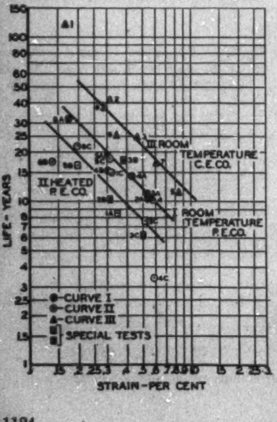


Figure 9 (left). Sheath life versus per cent strain at joint wipe based on tests in dummy manhole

Test points correspond to sample numbers listed in Tables III and V

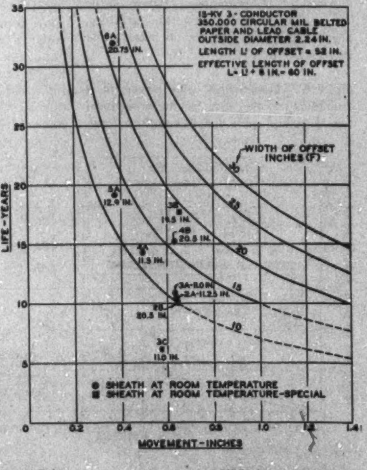


Figure 10 (right). Sheath life versus cable movement at each duct mouth in moderately loaded ten-foot-four-inch-long manhole

Test points are shown with respective widths of offset ance of cable core and sheath while the cable is under compression during the heating cycle. The tests so far have been conducted on only one type of cable, although further study is contemplated to determine the relative ability of various cable constructions to accommodate their expansion with minimum duct-mouth movement and most uniformly distributed snaking effects.

A flat dummy duct bank 96 feet long was built in the laboratory using $4^1/_4$-inch terra-cotta duct to explore these phenomena. All ducts were split so that the top of each section could be removed. The setup is shown in Figure 14. The cable was the same size tested in the dummy manhole. Sheath rings were cut in the south cable at 12-foot intervals in order to study any possible movement of the sheath relative to the core of the cable. The restraining forces on the two cables in parallel for the first four runs were 0, 200, 400, and 1,000 pounds. In the fifth run the restraining force was maintained just enough to prevent movement at the duct mouths. In each run the conductor temperatures were varied in steps of ten degrees centigrade from ambient temperature to 40 degrees centigrade above ambient temperature and back again. Readings were taken for each cable showing its position at six-foot intervals. A three-legged steel table shown in Figure 15 was moved along the duct bank to serve as bench marks for the measurements. Also, at successive steps the width of each ring gap in the south-cable sheath was measured.

Table III. Cable-Sheath Life in the Dummy Manhole

Sample No.	Make Cable	Sheath Temp (Deg C)	Width of Offset (Inches)	Cable Movement (Inches)	Life		Computed Strain at Joint Wipe (Per Cent)
					Cycles	Years	
Cable at Room Temperature*							
6-*A*	*A*	18	20.75	0.37	8,842	32.1	0.18
5-*A*	*A*	19	12.9	0.37	5,295	19.3	0.31
4-*B*	*A*	23	20.5	0.63	4,225	15.4	0.31
4-*A*	*A*	23	11.5	0.49	3,951	14.4	0.44
2-*A*	*A*	25	11.25	0.63	2,872	10.4	0.54
3-*A*	*A*	25	11.0	0.63	3,031	11.0	0.55
Cable Heated*							
5-*B*	*A*	35	29.0	0.42	4,852	17.7	0.14
6-*C*	*A*	35	20.25	0.41	5,948	21.6	0.20
5-*C*	*A*	36	10.4	0.32	5,062	18.4	0.32
1-*C*	*A*	40	17.5	0.56	4,167	15.1	0.33
4-*C*	*A*	42	10.6	0.65	946	3.4	0.59
Special Tests (More Than One Variable)							
2-*B*	*C***	25	20.5	0.65	2,872	10.4	0.32
3-*B*	*B*†	25	19.5	0.65	4,899	17.8	0.39
3-C	*A*‡	25	11.0	0.57	1,710	6.2	0.51
5-*B*	*A*⊗	44	20.1	0.40	4,524	16.6	0.20
1-*A*	*D*⊕	45	18.0	0.63	2,336	8.5	0.35
2-*C*	*C***	42	10.5	0.56	2,097	7.6	0.52

* Chemical lead sheath with one-half-inch sand-cement fireproofing.

** Copper lead sheath with one-half-inch sand-cement fireproofing.

† Possibly chemical lead sheath with one-half-inch sand-cement fireproofing.

‡ Chemical lead sheath with one-half-inch sand-cement fireproofing precracked before test run.

⊗ Chemical lead sheath with asbestos cement (one-fourth-inch thick) over felted asbestos wrapping.

⊕ Type lead sheath unknown; no fireproofing.

All tests were conducted on 15-kv three-conductor 350,000-circular-mil belted paper and lead cable with outside diameter of 2.24 inches.

Effect of Restraint on Movement at Duct Mouth

Figure 16 shows how the restraining force at the end of the cable had to be varied in order to maintain zero cable movement at the duct mouth with increasing and decreasing conductor temperatures. The apparent discontinuity in the cooling curve as it crossed the abscissa resulted from the continuing contraction of the cable during the interval required to reverse connections to the dynamometer. The reversed force was

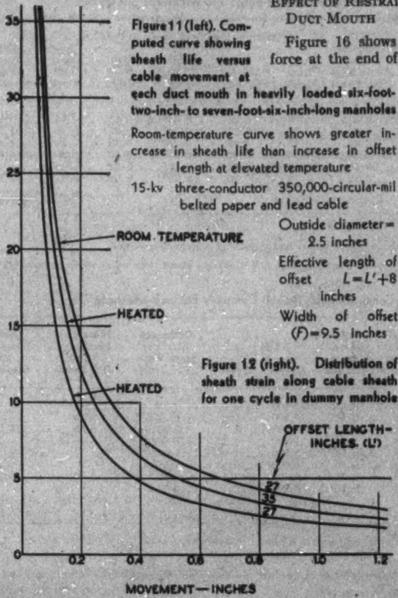


Figure 11 (left). Computed curve showing sheath life versus cable movement at each duct mouth in heavily loaded six-foot-two-inch- to seven-foot-six-inch-long manholes

Room-temperature curve shows greater increase in sheath life than increase in offset length at elevated temperature

15-kv three-conductor 350,000-circular-mil belted paper and lead cable

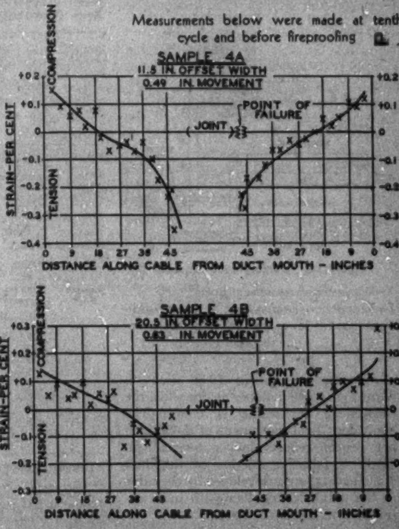
Outside diameter = 2.5 inches

Effective length of offset $L = L' + 8$ inches

Width of offset $(F) = 9.5$ inches

Figure 12 (right). Distribution of sheath strain along cable sheath for one cycle in dummy manhole

Measurements below were made at tenth cycle and before fireproofing



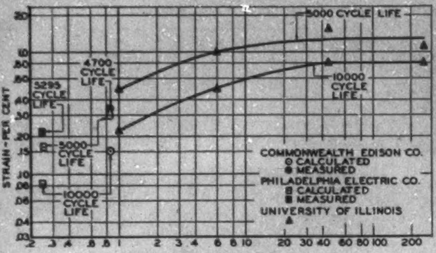


Figure 13. Comparison of strain data from tests by various investigators

Per cent strain values apply for only one half of the range of cyclic movement

needed to prevent the cable end from retracting into the duct.

As long as the restraining forces maintained at the duct mouth in the other test runs were small (compared with the values shown in Figure 16 to be required for complete restraint of each cable), there was little effect on longitudinal cable expansion. At 500 pounds restraint there was only a ten per cent drop in longitudinal movement noted in Figure 17 below the values for zero restraint. The curves confirm the movement–temperature-range linearity of the field-survey data shown in Figure 6. The movement at the duct mouth of 0.32 inch for a 25-degree-centigrade rise with a restraint of 500 pounds per cable (curve IV) corresponds to 0.040 inch per degree for a 300-foot section. The comparable unrestrained movement (0.39 inch on curve I) is 0.049 inch per degree while the theoretical bar value is 0.057. The equivalent field movement of 0.020 inch per degree reflects higher over-all restraint comprising friction in the duct and reacting forces developed by the cable in the manhole, together with more extensive snaking in the duct.

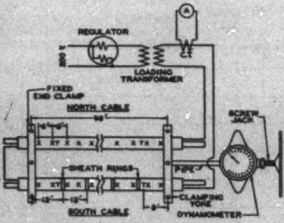


Figure 14. Dummy-duct test setup

X—Movement-measurement point
T—Temperature-measurement point—additional point in center of each cable not shown

Cable Performance in Partially Restrained Section

There was no unusual behavior of either cable inside the ducts with restraining forces from zero to 500 pounds. The peak lateral deflection was only 0.27 inch which indicated only a negligible amount of cable expansion in snaking. The longitudinal movements at the observation points along the cable toward the duct mouth showed a steadily accumulating motion. These restraining forces are of the same order of magnitude as shown in Figure 18 to be required to move the cable into the test manhole. This explains why no correlation could be established in the field survey between width of offset in the manhole and cable movements.

Figure 15. Cable-movement measuring table

Cable Performance in a Totally Restrained Section

When cable is totally restrained at the duct mouth, snaking is not uniformly distributed along the cable in the duct. Figure 19 shows that snaking can take place in a very short length of cable. The severe sheath strains measured at the location of the peak vertical move-

Table IV. Correlation of Computed and Measured Sheath Strain

Test No.	Sheath Life (Years)	Sheath Temp (Deg C)	Computed Strain at Joint Wipe (Per Cent)	Maximum Measured Strain in Per Cent: Left Duct Mouth	Left Wipe at Joint	Right Wipe at Joint	Right Duct Mouth	Avg at Joint Wipes	Ratio Computed to Avg Measured
5-*A*	19.3	19	0.31	0.08	0.28	0.44*	0.13	0.36	0.86
4-*B*	15.4	23	0.31	0.14	0.17	0.19*	0.29	0.18	1.72
4-*A*	14.4	23	0.44	0.15	0.35	0.27*	0.13	0.31	1.42
5-*C*	18.4	36	0.32	0.14	0.29	0.28*	0.13	0.29	1.10
5-*B*	16.6	44**	0.20	0.08	0.20	0.16*	0.24	0.18	1.11
4-*C*	3.4	42	0.59	0.15	0.49	0.40*	0.11	0.45	1.31
								Average†	1.25

* Failed at this location.

** Flexible asbestos fireproofing. All others had rigid sand-cement fireproofing.

† This average improves to 1.16 by ignoring test 4-*B* which has about same offset width as test 5-*B*.

Table V. Commonwealth Edison Company Dummy-Manhole Tests

Sample No.*	Width of Offset (Inches)**	Length of Offset (Inches)	Cable Movement (Inches)	Life: Cycles	Life: Years	Computed Strain at Joint Wipe (Per Cent)†	Measured Maximum Strain (Per Cent)	Ratio Computed to Measured
1	19	48	0.25	32,800	119.0	0.17		
2	19	48	0.50	11,500	41.8	0.32		
3	19	48	0.75	6,800	24.7	0.47	0.38	1.23
4	19	48	1.00	2,800	10.2	0.60		
5	9.5	48	0.75	3,100	11.3	0.85		
6	28.5	48	0.75	10,000	36.4	0.29		
7	19.0	33	0.75	4,700	17.1	0.62	0.65	0.95
8	19.0	66	0.75	6,900	25.1	0.35		
							Average	1.09

* Designations in Figure 9.

** Duct mouth to normal at joint wipe.

† Based on 2.44-inch outside diameter of 15-kv three-conductor 500,000-circular-mil belted paper and lead cable.

Table VI. Computations of Sheath Life on Station-Tie Cables in Various Manholes

Normal Peak—6,000 Kva; Daily Range—14.7 Degrees Centigrade; Average Total Duct-Mouth Movement—0.26 Inch

Per Cent of Total Lengths in Group*	Number of Sections of Cable	Per Cent of Total Cable Movement at One Duct Mouth*	Range of Movement at One Duct Mouth (Inch)	52-Inch-Long Offset (L') 10-Inch-Wide Offset			35-Inch-Long Offset (L') 9.5-Inch-Wide Offset∮			27-Inch-Long Offset (L') 9.5-Inch-Wide Offset∮		
				Range of Life of Group (Years)	Average Life of Group (Years)	Weighted Life of Each Group (Years)	Range of Life of Group (Years)	Average Life of Group (Years)	Weighted Life of Each Group (Years)	Range of Life of Group (Years)	Average Life of Group (Years)	Weighted Life of Each Group (Years)
8	29	0–25	0–0.06	Indeterminable	50	4.0	Indeterminable	50	4.0	Indeterminable	50	4.0
27	97	25–50	0.06–0.13	Indeterminable	40	10.8	40–18	29	7.9	28–13	20.5	5.5
34	122	50–75	0.13–0.19	27–19	23	7.8	18–12	15	5.1	13–10	11.5	3.9
17	61	75–100	0.19–0.26	19–15	17	2.9	12–10	11	1.9	10–7	8.5	1.5
8	29	100–125	0.26–0.32	15–12	13.5	1.1	10–8	9	0.7	7–6	6.5	0.5
4	14	125–150	0.32–0.39	12–10	11	0.5	8–7	7.5	0.3	6–5	5.5	0.2
1	4	150–175	0.39–0.45	10–9	9.5	0.1	7–6	6.5	0.1	5–4.5	4.8	0.1
1	4	175–200	0.45–0.52	9–8	8.5	0.1	6–5	5.5	0.1	4.5–4	4.3	
100	360	Weighted life of line—years				27.3			20.1			15.7

* These percentages are a composite evaluation of the probability factors in Table I and in the subdiagram in Figure 6.

∮ These columns are based on the curves in Figure 11.

This table applies to 15-kv three-conductor 350,000-circular-mil belted paper and lead cable with outside diameter of 2.5 inches.

ment are shown in Figure 20. Measurements of strain using the dial-type micrometer described in Appendix *B* showed a maximum strain at 40 degrees centigrade of 1.3 per cent. Within less than six feet the $2^1/_4$-inch-diameter cable rose about two inches to press against the top of the $4^1/_4$-inch duct and then dropped to contact the bottom of the duct again. On the cooling cycle the deflection could not be removed from the cable although a force of about 350 pounds was applied at the duct mouth to pull the cable longitudinally back to its starting point. The peak strain at room temperature measured 0.35 per cent, indicating a change (cyclic strain) of 0.95 per cent for an operating range of 40 degrees centigrade. Such a daily cyclic strain is seen on curve II of Figure 9 to indicate a sheath life of about four years. This cyclic strain was far in excess of the values measured in the dummy-manhole tests. Such unit strain may have produced the fatigue indicated in Figure 3.

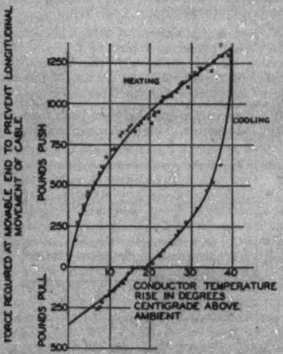


Figure 16. Force required to block cable movement at duct mouth of dummy duct

15-kv three-conductor 350,000-circular-mil belted paper and lead cable in $4^1/_4$-inch terra-cotta duct

Outside diameter of cable 2.24 inches

Snaking Versus Longitudinal Flexibility of Cable

With total restraint at the duct mouths, the cables had only one large region of snaking for conductor temperature rises above 25 degrees centigrade. Thus, it is seen that cable movement in expansion tends to concentrate at the location where snaking first starts. Figure 19 shows consistently accumulating increments of longitudinal movement from the fixed ends of the cable toward this region of peak snaking. The ringed south cable however did not have a similar longitudinal movement. The widths of the sheath-ring gaps were reduced (0.17 inch for seven gaps) when a totally restraining force was maintained at 40 degrees centigrade rise, but they did not change for any conductor-temperature rise at lower values of duct-mouth restraint. The radially expanded insulated conductors apparently gripped the sectionalized sheaths and carried them along the cable belt. It is significant that the ring gaps opened to their initial widths when the cable returned to room temperature.

The movements at the duct mouths of the continuous sheath and ringed-sheath cables at zero restraint were 0.62 and 0.59 inch, respectively, for a 40-degree-centigrade rise. The expansion at total restraint must have been absorbed in the compression of the copper, in snaking, and in radial expansion. A careful analysis of snaking along the cables seems to account for little more than 0.2 inch on the north cable and 0.1 inch on the south cable. Snaking therefore would seem to have accommodated no more than about one third of the normal longitudinal expansion of the cable at the duct mouth. On the other hand, cold tension and compression tests indicated a degree of flexibility of the cables that apparently accounted for little more than one third of the zero-restraint expansion in radial expansion of the cable while under compression. It is therefore impossible because of masking influences to define clearly from the present data in what exact proportions the component parts of the free expansion was distributed in snaking, radial expansion, or other

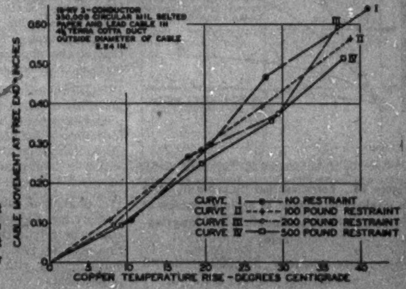


Figure 17. Movement versus temperature rise with various restraining forces at one end of cable with other end permanently fixed

masked phenomena under full restraint.

The ringed south cable however appeared to have been rendered more flexible longitudinally since it must have expanded radially to account for the one tenth of an inch difference in longitudinal expansion from that of the north cable. Although too much weight cannot be placed upon these measurements because of their size and the manner in which the ring gaps could have opened, it is significant that the compressive force of total restraint did make the sheath move relative to the cable belt.

It appears that a cable whose core is susceptible to a greater amount of radial expansion will produce less snaking in the stretch and also less movement at the duct mouth. Whether the resulting over-all changes in cyclic strains in the lead sheath will be beneficial remains to be ascertained by a careful weighting of all the factors.

Conclusions

1. Unsatisfactory performance of paper-insulated lead-covered cable can result from daily operation at temperatures within the limits in regular Association of Edison Illuminating Companies specifications.

2. The rate at which sheath cracks occur varies through a wide range so that a high failure rate on a line does not indicate that the useful life of all of the associated cable has been expended.

3. Cable movement at the duct mouth increases linearly with conductor-temperature rise and is the same for all cable lengths above 300 feet within a 25-degree-centigrade range.

4. Total movement generally divided unequally between the two ends of the cable. About 76 per cent of the cable sections had from 50 to 70 per cent of the total duct-mouth movement at one end.

5. Cable movement at the end of a flat 96-foot duct run showed little effect of restraining forces at the duct mouth up to 500 pounds per cable for conductor temperature ranges up to 40 degrees centigrade.

6. Snaking of cable inside a flat 96-foot conduit run in the laboratory was negligible for conductor temperature rises up to 40 degrees centigrade with restraining forces at the duct mouth less than 500 pounds per cable.

7. Snaking of cable within the duct appears to account for only about one third of the equivalent zero-restraint movement at the duct mouth for a 40-degree centigrade rise in conductor temperature.

8. The test data is inconclusive regarding the evaluation of the remainder of the theoretical free expansion not taken up in snaking, but apparently absorbed in radial expansion and other phenomena produced in the conductors by compression when the cable is fully restrained at the duct mouth.

9. The desirability of securing more uniform snaking of the cable in the duct and a reduction of cable movement at the duct mouth appears to justify further investigation of the physical properties of various types of cable construction.

10. Distortion of the cable core under compression and snaking in the middle of a cable section in excess of 300 feet in length account for failure of long lengths to expand at the duct mouths more than a 300-foot section within a 25-degree centigrade conductor-temperature range.

11. Snaking of cable in the duct can occur in short enough lengths to cause substantial strains in the sheath with consequent reduction of cable life. Deflections are vertical and sidewise, with practically no rotation of the cable.

12. Cable-sheath life in the manhole is improved by increase in length and width of the offset and by reduction of cable movement.

13. Cable-sheath life is reduced substantially by increase in lead temperature within the present normal operating range.

14. There was not a sufficient number of samples of different kinds of lead represented in the dummy-manhole tests to warrant a conclusion regarding their effect on the strain–life relationship.

15. Substantial agreement existed between the actual operating record of a group of station tie-line cables and the computed sheath life determined from the data secured in the field survey and the laboratory tests.

16. The relationships between peak loads, daily load factors, cable movement, manhole

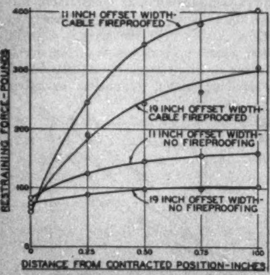
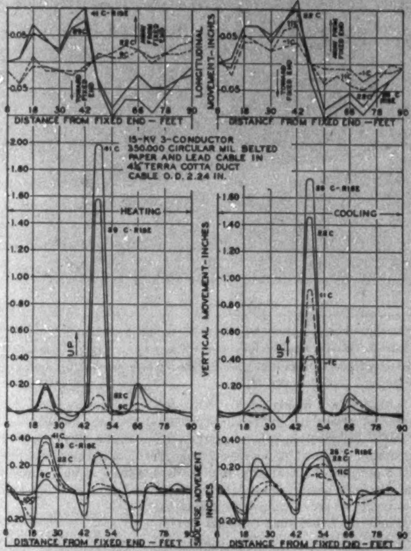


Figure 18. Force exerted by cable in offset in manhole in restraint of cable movement at duct mouth into a ten-foot-four-inch-long manhole

15-kv three-conductor 350,000-circular-mil belted paper and lead cable

Figure 19. Movement of cable with continuous sheath in duct with total restraint at duct mouth



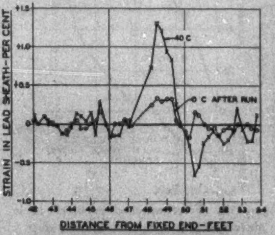


Figure 20. Measured sheath strains at location of peak snaking in dummy duct during and after a heat cycle

Cable totally restrained at duct mouth

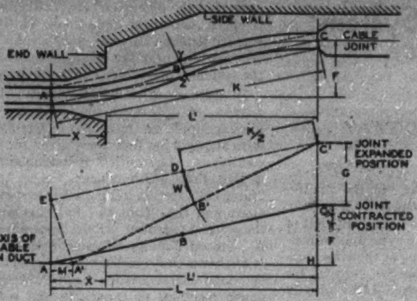


Figure 21. Analysis of movement of cable as a deflected cantilever beam

sizes, and sheath life have been sufficiently established for a major class of underground cable to warrant restricting the trend toward higher ratings by means of limits on daily cable movement, except where required as a wartime expedient.

Appendix A. Evaluation of Maximum Unit Strain in Cable Sheath

The usual method of racking cable in a manhole, shown in Figure 21 (upper sketch), provides an offset of width F and length L' which accommodates the movement resulting from expansion and contraction of cable in the duct. When the cable in the duct moves into the manhole, the cable joint moves toward the side wall. It is assumed in this analysis that the axis of the joint in the expanded position will be parallel to its original (contracted) position. During this motion of cable and joint, the cable in the offset will be bent. It is found on test that a greater reduction in radius of curvature takes place between points B and C. Since change in radius of curvature is a factor in the determination of strain (see Appendix B), attention is directed to the analysis of the amount of bending near the joint.

The distance from the end wall to point A at which the bending starts is indicated as X. Therefore, the effective length of the offset is L, the sum of X and L'. Measurements in the laboratory showed that the distance X was nearly a constant which could be evaluated at about eight inches. This value was used in the computations.

To simplify the analysis the single-line analogy in the lower sketch of Figure 21 is assumed. The solid line ABC represents the original (contracted) position of the cable. (This refers to the equivalent straight section of cable, whose length is K, shown by the broken lines in the upper sketch.) The heavy dotted line $AA'B'C'$ represents the position of the cable in the expanded position where AA' or M is the longitudinal movement at the duct mouth. This is reasonably correct, since the instantaneous centers of rotation for incremental amounts of bending can be represented as being pivots. It is apparent in the upper sketch that, as the cable expands, the lead sheath facing the side wall of the manhole is in compression between the point Y and the duct mouth and in tension between Y and the joint wipe. The analysis is confirmed by measurements of strain, which also show that the unit strain is zero at Y (approximately the mid-point of the offset).

The cable in the half of the offset adjacent to the joint wipe can be considered as a cantilever beam of length $K/2$ whose fixed support is at C'. This satisfies the condition that the unit strain is maximum near the joint wipe. The force W producing the bending moment at C' passes through D, which conforms with the zero unit-strain condition at points Y and Z on the sheath.

If the cable joint could have moved from C to C' without straining the lead sheath, it would have been necessary for the cable to move from its position ABC to a position EDC', which would be parallel to ABC. The actual effect of the point A moving to the position A' as far as strains in the sheath near the joint wipe is concerned is as if the cable moved through the angle $EC'A'$ around the point C'.

In applying the cantilever-beam-deflection formula to this movement of the cable offset, the distance $DC' = K/2$ represents the length of the beam and DB' the deflection due to the force W applied as shown at the point D. The value of W is not material in this analysis, since it can be eliminated from the equation for unit strain. It is a component of the force actually operating along the axis of the cable causing the cable movement.

Therefore:

$$\text{Deflection } DB' = W(DC')^3/3EI = WK^3/24EI \qquad (1)$$

where E = modulus of elasticity of the cable structure as a unit and I = moment of inertia of the cable

The beam formula, of course, holds where the beam is made of an elastic material. Obviously, lead is not a truly elastic material, but it has been assumed that a piece of cable when bent will follow the elastic curve for which the cantilever formula was derived. This appears to be permissible, since the copper in the cable would establish the curvature, and the lead sheath merely would follow. It is not necessary to determine the magnitude of E and I, since they also will be eliminated from the final equation for unit strain.

The radius of curvature R at any point along an elastic beam can be shown to be equal to EI divided by the bending moment at the point for which R is determined. Since we are interested in the bending radius at point C' resulting from the force W, the bending moment is equal to $WK/2$. Substituting these values in equation 1, we have

$$R = K^2/(12DB') \qquad (2)$$

It is shown in Appendix B that for a cable with outside radius r the per cent strain is equal to $100r/R$. Substituting the value of R from equation 2 in this expression, we have

$$\text{Per cent strain} = 1{,}200r(DB')/K^2 \qquad (3)$$

A further approximation for the sake of simplicity of the geometric relationship is made to the effect that the lines EDC' and $A'B'C'$ remain straight as they are deflected through the angle $EC'A'$. Since DC' was already assumed to be equal to $EC'/2$, it follows that $DB' = EA'/2$. Moreover, $AA' = M$; and $EA = CC' = G$.

If we solve the right triangles EAA', $A'HC'$ and AHC,

$$EA' = \sqrt{G^2+M^2} = 2DB' \qquad (4)$$

$$G = \sqrt{K^2-(L-M)^2} - F \qquad (5)$$

$$K^2 = L^2+F^2 \qquad (6)$$

Combining equations 3, 4, 5, and 6, we have

$$\text{Per cent strain} = \frac{600\sqrt{2}r\sqrt{F^2+LM-F\sqrt{F^2+2LM-M^2}}}{L^2+F^2} \qquad (7)$$

Appendix B. Direct Strain Measurements

The instrument used to measure the bending radius of the cable is shown in the upper part of Figure 22. The principle of opera-

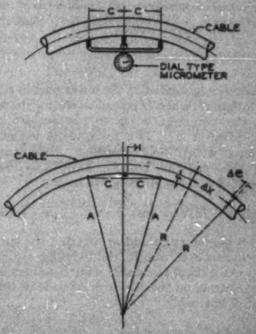


Figure 22. Determination of bending radius and strain in a bent cable using a dial micrometer

Strain = $\Delta e/\Delta x$

By construction $\Delta X/R = \Delta e/r$

Strain = r/R

Per cent strain = $100r/R$

Capacitors, Condensers, and System Stability

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THE need for increased system kilovar capacity coincident with electrically powering our national war effort rapidly with the minimum use of critical materials and production facilities has led to the installation of capacitors in banks of the order of 25,000 kva in various locations[1] in the United States.

At present price levels,[2] the economic choice between a capacitor bank and a synchronous condenser favors the latter in a size range somewhat below this, the critical size depending largely on the evaluation of losses, but since the trend of the price ratio of these two equipments is in the direction of increasing the economical relative size of capacitor bank, it is worth while to compare it with a synchronous condenser from the standpoint of its effect on system stability, and at the same time to bear in mind other differences in performance of the two equipments.

This study therefore—in the main—compares the performance of a system using a large capacitor bank with that of the same system using a synchronous condenser of the same rating at the same location. Two general types of systems are studied. A comparison is made also in one type of system for the conditions of

(*a*). No kilovar supply in load areas.

(*b*). Use of capacitors in load areas.

(*c*). Use of synchronous condensers in load areas.

Basically, it is recognized that either a capacitor or a condenser tends to or does reduce the excitation required on the generators on a system, in order to hold the necessary voltage levels. In the case of steady-state stability this is not particularly important where load increases occur gradually enough that they may be readily followed by the generator voltage regulator, be it the operator or an automatic device. Where quick load swings cause unregulatable voltage dips,[3] this reduction in excitation reduces somewhat the ultimate steady-state limit in a system operated near its steady-state power limit. The condenser's ability to hold up the system voltage during these swings, thus raising the synchronizing power, counteracts this tendency to reduction of power limit. The capacitor, on the other hand, abets the tendency by reducing the synchronizing power because it lets the voltage sag. It is shown, however, that the difference between the two is small, hence the importance of this small difference depends to a great extent on how near the steady-state limit the system is operating.

In the case of transient stability, the relative limits between the two types of kilovar supply are measured in terms of critical switching time for a given set of conditions. The critical switching time is that time which equally weighs the "fault-on" forces pulling the systems apart and the "fault-off" forces pulling the systems together. (Refer Appendix—A Note on Factors Affecting Stability.) It is shown that for the type of systems and load characteristics studied the opposite reactions of capacitors and condensers on these two opposing forces have a small comparative effect in general on the critical switching times.

Scope and Method of Analysis

In making a study of this type it is necessary to get down to cases in the last analysis. The results then necessarily apply rigorously to only the cases chosen, and there are probably as many specific types of systems as regards detail as there are actual systems. There are two classifications, however, into one of which most systems fall, and as these two types were studied, it is felt that proper interpretation of the results will present a reasonably broad qualitative picture.

The systems studied are defined as:

Type 1. Noninterconnected system—comprises a metropolitan-type system with steam generation in the load area, connected to a distant hydroelectric station with a large kilovar source at the receiving end of the transmission line (Figure 1).

Type 2. Interconnected system—comprises, in principle, two large systems connected by a two-circuit transmission line

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The authors acknowledge the assistance of N. H. Meyers and J. D. Phillips in obtaining and analyzing the network-analyzer results.

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tion is demonstrated in the lower part of the same figure. When H is read on the micrometer, the bending radius to the surface of the cable is A. The instrument is constructed with $C=\sqrt{10}$ inches. Substituting this in the relationship, $A^2=C^2+(A-H)^2$, and simplifying, we have

$$A=5/H+H/2 \text{ inches} \tag{8}$$

It is necessary to add or subtract the outside radius r to obtain the bending radius R to the axis of the cable when the instrument is used on the inside or outside of the bend of the cable. Thus,

$$R=5/H+H/2\pm r \text{ inches} \tag{9}$$

Figure 22 also shows the derivation of the fundamental strain formula:

$$\text{Per cent strain}=100r/R \tag{10}$$

This follows the analogy of a bent cable to a bent beam. The incremental elongation of the lead on the outside radius of the sheath is shown related to an original infinitesimal straight length of cable which remains the length along the neutral axis after the cable is bent with a uniform bending radius. This derivation is based on the assumption that a plane section through any point on the cable and extending through the center of the radius of curvature remains a plane after the cable has been bent.

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Table I. Individual Machine Constants Based on Rating

	Hydroelectric	Steam	Condenser
Rating	25,000 kva, 1.0 pf	31,250 kva, 0.8 pf	50,000 and 75,000 kva
X_d'	0.20 to 0.45	0.23	0.40
H (seconds)	4.5 to 7.5	5	1.9
X_{eq}	0.7	0.75	0.7

Table II. Effect on Critical Switching Time of Fault Location

Kilovar Supply at Substation A	Kilovar Supply in Load Area	Three-Phase Fault Location	Critical Switching Time, Cycles
Capacitor	Capacitor	33-kv bus—substation *B*	46.4
		33-kv bus—substation *A*	11.9
		Hydroelectric station high voltage	8.48
Condenser	Capacitor	33-kv bus—substation *B*	∞
		33-kv bus—substation *A*	12.6
		Hydroelectric station high voltage	8.1
Capacitor	Condenser	33-kv bus—substation *B*	∞
		33-kv bus—substation *A*	12.1
		Hydroelectric station high voltage	7.53
Condenser	Condenser	33-kv bus—substation *B*	∞
		33-kv bus—substation *A*	12.8
		Hydroelectric station high voltage	7.55

with an intermediate switching station and kilovar source (Figures 11 and 12).

For type 1 the effects on the transient stability limits of the hydroelectric-station inertia, transient reactance, initial load, fault-clearing times, supplying kilovars in the load area by capacitors or synchronous condensers, fault location, the size of receiving system, and the load characteristics are determined for either type of kilovar source of various ratings (condenser or capacitor) located at the receiving end step-down substation. The effect on steady-state stability of supplying kilovars in the load area by capacitors is also determined for the two types of kilovar sources at the step-down station.

For type 2 the effects on the transient stability limits of the relative sizes of the two systems for either type and rating of intermediate kilovar source are determined. Also, the effect on the steady-state limits of the type and rating of intermediate kilovar source is studied.

The a-c network analyzer was used in obtaining most of the results in this work, and the basic data pertains in general to 60-cycle systems. Conventional methods of swing-curve calculation and the equal-area criterion[3] were used in determining critical fault-clearing times. This approach allowed several variables to be explored efficiently. In obtaining the torque-angle data from the analyzer for the synchronous-condenser case, the condenser was represented by a capacitor in series with a reactance equal to the transient reactance of the machine, the voltage back of transient reactance being maintained by cutting in more or less capacitors as required. The accuracy of this method for the case at hand is established in the Appendix. In the case of the capacitors, the initial conditions were set up and the capacitor setting was then untouched as the torque-angle data were obtained.

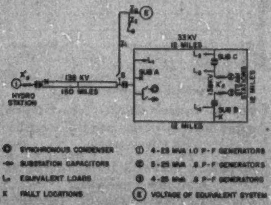


Figure 1. Schematic diagram of type-1 (noninterconnected) system studied

Table I gives the values and ranges of the parameters, and the machine ratings used in determining network analyzer box settings for the type 1 system study. The operating data and design details of this system have been reported elsewhere,[4] wherein it is recorded that the steam-turbine generator operating power factor with the full complement of shunt capacitors in the distribution system is approximately 0.90 per unit. The steam generator power factor without capacitors was 0.76 per unit. The hydroelectric generators are operated at approximately 1.0 power factor in all cases.

Conclusions

1. The difference between the effects on transient and steady-state stability of large capacitor banks and synchronous condensers is small. For the type 1 system studied constant-impedance loads and/or the longer critical switching times tend to favor the capacitor while the composite loads and/or shorter critical switching times tend to favor the condenser.

2. Either type of kilovar supply in radial load areas tends to reduce stability when this results in a reduction of the excitation on the generators.

3. When installed at the junction points of interconnected systems neither synchronous condensers nor capacitors having a rating corresponding to the emergency kilovar requirements have much effect on stability. Appreciable improvement can be obtained with condensers if a rating greatly in excess of the kilovar requirements—thus presenting a low reactance—is used.

4. The system voltages during a fault and during the swings following fault removal will be better maintained by a small amount when using a condenser than when using a capacitor.

Noninterconnected System (Figure 1)

TRANSIENT STABILITY

Effect of Fault Location. Preliminary to exploring the several parameters for this system, critical switching times were calculated for faults in three different locations for definitely determining the fault location presenting the greatest stability problem. The results are listed in Table II for constant-impedance load and hydroelectric-generator unit parameters on their own rating of:

$H = 4.5$ seconds
$X_d' = 0.35$ per unit
Approximate hydroelectric load = 90,000 kw
Approximate steam load = 200,000 kw

For this type system, these data show that:

(a). The most serious fault location is at the sending end of the high-voltage line.

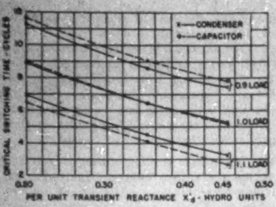
(b). The difference in critical switching time between a capacitor and a condenser at substation *A* for either capacitors or condensers in the distribution areas is small.

A three-phase fault at the hydroelectric end of the 138-kv line, causing loss of one line section, is used therefore as the standard test for the rest of the comparison.

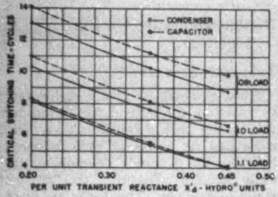
Effect of Supplying Kilovars in the Load Areas. The effect on transient stability supplying part of the kilovar requirements locally in the load areas is indicated by the data in Table III. These data compare the critical switching times required for either synchronous condensers or capacitors in the load areas with no cor-

Table III. Effect on Critical Switching Time of Supplying Kilovars in Load Areas

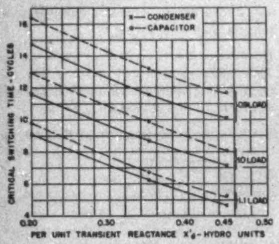
Kilovar Supply at Substation A	Kilovar Supply in Load Areas	Critical Switching Time, Cycles
75,000-kva condenser	None	8.9
75,000-kva capacitor	None	8.9
50,000-kva condenser	90,000 kva of capacitors	8.1
50,000-kva capacitor	90,000 kva of capacitors	8.48
50,000-kva condenser	90,000 kva of condensers	7.55
50,000-kva capacitor	90,000 kva of condensers	7.53



(a). Inertia constant $H=4.5$



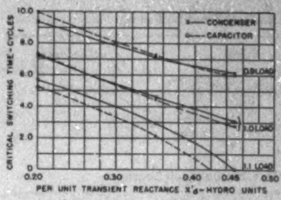
(b). Inertia constant $H=6.0$



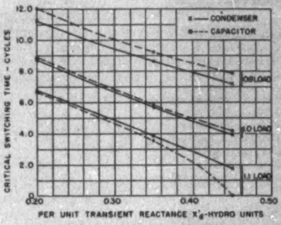
(c). Inertia constant $H=7.5$

Figure 2. Transient stability with impedance load and no distribution capacitors

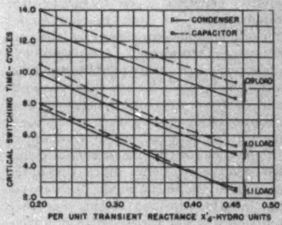
Effect of varying initial hydroelectric load in per unit of station rating and transient reactance



(a). Inertia constant $=4.5$



(b). Inertia constant $=6.0$



(c). Inertia constant $=7.5$

Figure 3. Transient stability with impedance load and distribution capacitors

Effect of varying initial hydroelectric load in per unit of station rating and transient reactance

rection at all in the load areas. The parameters for this case are:

$H=4.5$ seconds
$X_d'=0.35$
Approximate hydroelectric load = 91,500 kw
Approximate steam load = 200,000 kw
Fault location—sending end of 138-kv high-voltage line. Constant-impedance load.

Other comparisons for this case can be obtained by comparing the data in Figures 2, 3, and 8. Figure 2 indicates the effect on critical switching times of varying the hydroelectric-generator parameters for the case of no kilovar supply in the load areas while Figure 3 gives the same information for approximately 90,000 kva of capacitors installed in the distribution area.* Figure 8 gives similar data for a composite load characteristic.

Effect of Hydroelectric-Generator Characteristics. It is generally good economical practice to install hydroelectric units with inertia which is standard for the particular transient reactance specified, but with a combination giving the required stability characteristics. In this study, however, the individual parameters were varied in the interests of determining the effects of each, recognizing that in the last analysis the practice previously mentioned should apply.

Critical, or required, switching times for the case of an impedance load characteristic without distribution capacitors comparing a condenser and capacitor at substation *A* are shown in Figure 2 for a variation in hydroelectric-generator characteristics on rating of

$X_d'=0.20$ to 0.45 per unit
$H=4.5$ to 7.5 seconds

Similar data for the case of approximately 90,000 kva of capacitors in the distribution areas are given in Figure 3.

These data can be cross plotted in different ways to portray the variations in a different fashion if desirable.

It is interesting to note that generally where the switching times are relatively long, the differential in switching time between the capacitor and condenser is larger, in favor of the capacitor, and increases with increase in switching time, while in the regions of small switching times the differential is in favor of the condenser and increases with reduction in switching time. It is felt, however, that the differential in any case is generally of negligible importance. These effects are discussed in more detail in the appendix under "A Note on Factors Affecting Stability."

Effect of Kilovar Source Rating at Substation A. More or less of the kilovar requirements at substation *A* can be supplied from the generators at substations *B* and *C*, thus altering the rating of the kilovar source at that location. The effect of the rating of the substation *A* kilovar source on the critical switching time for clearing a fault at the sending end of the transmission line is shown in Figure 4. These data are prepared for the particular set of conditions as shown in the figure. The critical switching time in this instance is increased less than one-half cycle by supplying all the kilovar requirements of substation *A* by a local kilovar source over that of supplying all the kilovars from the generators at substations *B* and *C*. The difference between a capacitor or condenser kilovar source at this location is of negligible significance on the critical switching time.

Effect of Load Characteristic. In an endeavor to obtain at least a qualitative indication of the effect of a composite load in the comparison being made, a synthetic load characteristic comprising one-half impedance loads and one-half induction motors delivering constant-torque loads was used. Since all con-

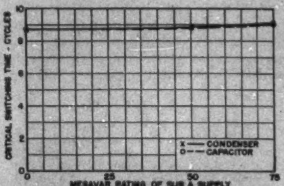


Figure 4. Transient stability, type-1 system, showing effect of varying rating of substation *A* kilovar supply

Hydroelectric load = 0.9 per unit. No distribution capacitors
Inertia constant $H=4.5$ seconds. Impedance load
$X_d'=0.35$ per unit

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* Table III data do not necessarily agree exactly with the corresponding cases in Figures 2 and 3 because the two setups were not identical. Table III setup was arranged to accommodate the condenser–capacitor comparison, their location being at the load area substations, and the load magnitudes and allocation of kilovars and kilowatts between stations was not duplicated in the two cases.

nected motors do not operate at full load at time of system peak, it was further assumed that half the induction motor load was operating at one-half load and the other half at full load.

The induction-motor equivalent circuit of Figure 5 is representative of a 200-horsepower 2,300-volt 1,800-rpm general-purpose squirrel-cage induction motor. The characteristics of such a motor were assumed to be representative of the composite motor characteristics of all the sizes and types of actual motors in the load areas. From this equivalent circuit, with the assumptions of the previous paragraph, the kilowatts and kilovolt-amperes versus voltage characteristics of Figure 6 were determined, and the network analyzer load boxes were adjusted in accordance with those data when the torque-angle curves were taken for determining critical switching times.

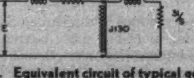


Figure 5. Equivalent circuit of typical general-purpose 200-horsepower 2,300-volt 1,800-rpm squirrel-cage induction motor

Circuit constants in ohms referred to primary—full-load normal-voltage slip approximately 0.011 per unit

During periods of low voltage, the undervoltage releases may take certain induction motors off the line. The voltages obtaining at substations *A* and *B* during and subsequent to the fault are shown in Figure 7. The voltage at substation *A* reaches a value that could take off some of the motors; hence to get a fair comparison from the stability standpoint, all the motors at substation *A* for both the condenser and capacitor case were removed during the fault, for the data given in Figure 8.

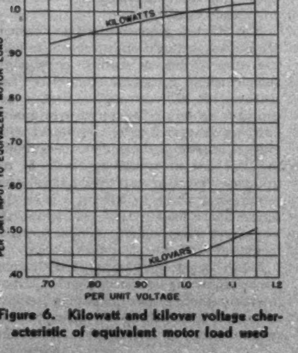


Figure 6. Kilowatt and kilovar voltage characteristic of equivalent motor load used

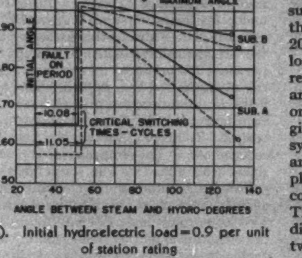
These composite load cases seem to generally show less differential between the condenser and the capacitor than the corresponding impedance load cases, indicating the increased relative importance of the condenser characteristics for composite loads.

Since the condenser maintains a higher voltage than the capacitor during the disturbance, it would be expected that not so many induction motors would be tripped off for the condenser case. This could be very important if there was sufficient stability margins obtaining in the system to permit retaining the load. To indicate the effect of not tripping off any induction motors at substation *A* on the critical switching times for the condenser case, the data in Table IV are given.

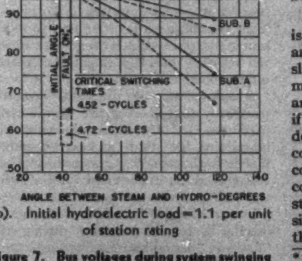
It is obvious that the more load that is held on to, the less will be the critical switching time, and for this type system and conditions assumed, the difference amounts to little more than three cycles.

Refer to appendix for further discussion of load characteristics and factors affecting stability.

*Effect of Size of Receiving System Generators and Loads.** The starting point for this part of the study corresponds to the basic type 1 system used extensively elsewhere, namely, a system of approximately 300,000 kw load, 100,000 kw being supplied by the hydroelectric plant through 150 miles of 138-kv line and 200,000 kw by the steam plants near the load areas. To indicate the effect the relative sizes of these two power sources and the magnitude of the load might have on the results, the data in Figure 9 are given. To obtain these data the total system loads were arbitrarily increased and supplied by enlarging the steam plants to the required values, taking due consideration of inertia and reactance. These data indicate that there is little difference in critical switching time between the condenser and capacitor at substation *A* as the loads and steam plants are increased in capacity.



(a). Initial hydroelectric load = 0.9 per unit of station rating



(b). Initial hydroelectric load = 1.1 per unit of station rating

Figure 7. Bus voltages during system swinging subsequent to fault-clearing

Impedance load. Distribution capacitors. $X_d' = 0.35$ per unit. $H = 7.5$ seconds

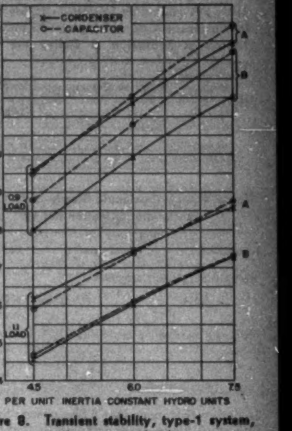


Figure 8. Transient stability, type-1 system, with composite loads

Effect of varying initial hydroelectric load and inertia

Curves *A*—With no distribution capacitors

Curves *B*—With distribution capacitors

STEADY-STATE STABILITY

The test used for this type of stability is in principle that presented by Clarke and Lorraine.[6] This involves making a slight increase in the angle of a particular machine from an operating point without any change in excitation and determining if it loses or picks up load. This was done on the network analyzer for the three conditions listed below. These various conditions were taken in the interests of completeness, realizing that this type of stability is more or less artificial as such, since the system could not operate at these values, but it does give a measure

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* This case is different from that presented in Figure 11 in that the loads were increased, and the generator inertia and reactance were changed to conform to rating. In Figure 11 the steam-generator inertias only were arbitrarily increased to simulate a distant large interconnected system.

Table IV. Effect on Critical Switching Times for Condenser Case of Tripping Off Induction Motors at Substation *A* During the Fault

Approximate Initial Hydroelectric Load, Kw	Induction Motors Tripped Off, Cycles	Induction Motors Not Tripped Off, Cycles
92,000	9.5	6.2
110,000	6.2	3.0

Table V. Hydroelectric Steady-State Limits in Per Unit of Station Rating

Condition	Condenser	Capacitor
With Distribution Capacitors		
a	1.57	1.41
b		1.42
c	1.14	1.09
Without Distribution Capacitors		
a	1.51	1.4
b		1.42
c	1.09	1.06

of operating margins. The power limits for these three cases as listed in Table V were determined for the conditions with and without distribution capacitors in the distribution system.

CONDITIONS STUDIED

(*a*). Terminal voltages of all synchronous apparatus maintained except for the last small excursion in angle.

(*b*). Terminal voltages of generators and the capacitor bank at substation *A* maintained. Capacitor terminal voltage maintained by cutting in more capacitor kilovolt-amperes.

(*c*). Voltages back of equivalent synchronous reactance of all synchronous apparatus maintained.

These power limits apply for all lines in service. The limits are reduced approximately three per cent for one line section out. In every instance the condenser gives a larger margin than the capacitor. This results from the inherent characteristic of the condenser to resist a change in the system 60-cycle voltage.

The small gain in going from *a* to *b* may be explained by the fact that the limiting angle was only 60 degrees for condition *a* and 65 degrees for condition *b*, indicating that only a small sagging of the voltage at substation *A* was experienced at the limit.

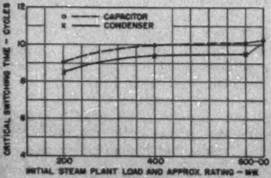

Figure 9. Transient stability, type-1 system, showing effect of varying loads and steam-plant rating

Interconnected System

The system studied and discussed in the previous section was labeled as noninterconnected although it was shown that the principal stability problem concerned an interconnection between a hydroelectric station and a metropolitan load center. These major elements were considered as a single system, but the results of course could apply to a specific type of interconnection in which load and kilovar source is associated with only one system, and straightaway transmission is involved.

EFFECT ON STABILITY WITHIN ONE SYSTEM OF INTERCONNECTING WITH ANOTHER SYSTEM

Considering again the noninterconnected system, it was desired to compare the effect of a condenser and a capacitor bank at substation *A* when the system was interconnected with another, say by closing switch *S* in Figure 1. The effect of interconnection on the transient stability of the tie between the hydroelectric plant and the metropolitan system may be evaluated by simply adding the inertia of the interconnected system to that of the metropolitan system and proceeding with the conventional method of analysis.

This may be done on the basis of assuming there is considerable interconnection impedance Z_t between the system under consideration and the system to which it is tied, and further assuming the the interconnection will not trip out for transient disturbances on the hydroelectric-plant line. Then, applying the wye-delta impedance transformation to the impedances of the interconnecting system, Z_t, Z_1, and Z_6, this system may be reduced to a line impedance, a small shunt load added to the shunt load at substation *A* and a large shunt load against a machine of zero impedance. This location of the large shunt load, and the magnitude of the small shunt load, enables them to be discarded in the limit. Since it is assumed the interconnection will maintain synchronism, its only effect remaining to be considered on the basis of these assumptions is its inertia. This simplification is often used to represent an extensive interconnection where the stability problem is external to the interconnection.

Results of varying the value of metropolitan-system inertia are summarized in Figure 10. These curves disclose the interesting fact that, in general, adding inertia to the receiving system results in a slight decrease in required fault-clearing time, thus somewhat increasing the stability problem. The effect of the added inertia is to aid stability during the "fault-on" period by reducing the slowing down tendencies of the receiver system. After the fault is cleared, however, it also decreases the ability of the receiver system to pull up or synchronize with the overspeeding sending-end generators, an effect opposing stability. Figure 10 indicates that the latter effect carries more weight than the former as the receiver inertia is increased, resulting in a net decrease in critical switching time in all cases except the case of substation *A* condenser and no distribution capacitors. Here the inertia effects appear practically to be neutralized by the synchronizng power increase permitted by the condenser, regardless of the receiver inertia considered.

STABILITY OF THE INTERCONNECTION WITH INTERMEDIATE KILOVAR-SOURCE CAPACITY

Power systems in the United States are becoming more and more interconnected in the interests of over-all economies of investment and operating costs. Where the interconnection between two systems or groups of systems also supples load areas along its route, it becomes necessary to use intermediate kilovar-source capacity to hold the voltage at these intermediate loads to acceptable values for power flow either way in varying amounts between the systems.[6] Thus, these intermediate kilovar sources are called upon to supply kilovars to the intermediate loads and the I^2X requirements of the interconnection, under both normal and emergency conditions.

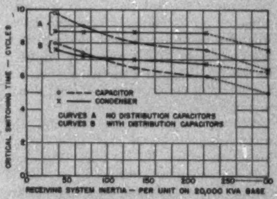

Figure 10. Transient stability, type-1 system, showing effect of arbitrarily increasing receiving-system inertia, simulating an extensive interconnection

It is of interest to compare the condenser and capacitor as an intermediate kilovar supply from the stability viewpoint, since it will be recognized that either can perform the kilovar-supply function acceptably assuming the control equipment switches on capacitors in accordance with voltage requirements, and further assuming the system regulating problem requires no "lagging" capacity. This latter requirement may be absent where the intermediate load is large enough to always have a considerable kilovar requirement.

It has been pointed out previously that the arrangement considered under "Noninterconnected System" may be looked upon as a special type of interconnected system without intermediate load but

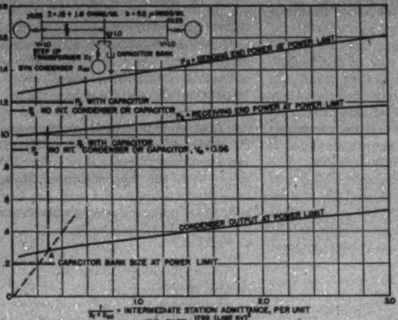

Figure 11. Steady-state stability, type-2 system, comparing effects of synchronous-condenser rating with capacitors and no kilovar source at intermediate station

Point *A* on condenser output curve defines condenser rating whose rated output is used at power limit, based on $X_t+X_{ec}=1.0$ on rating

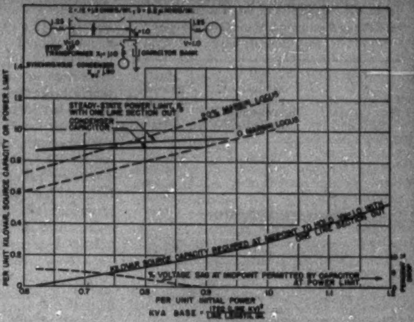

Figure 12. Steady-state stability, type-2 system, comparing intermediate condenser and capacitor of rating dictated by initial power transfer

with the load and kilovar-source capacity concentrated at one end. It is felt that the general transient stability results for that system will apply to the general type of interconnection considered here; hence in the interests of condensation this section will consider only steady-state stability effects.

Figure 11 shows the per-unit steady-state power limit of a two-circuit interconnection between systems having equal representative reactances, as a function of intermediate kilovar-source capacity. The kilovolt-ampere base applying depends upon the length and voltage of the interconnecting transmission lines and is determined by the relationship shown on the figure (see appendix for further discussion of this generalization). Table VI is given for ready reference to show the kilovolt-ampere base applying to various voltages and line lengths, based on this relationship. In Table VII the relationship for determining required condenser rating associated with the curves as a function of the other variables is given, and example results for a 300-mile 132-kv interconnection with a certain value of transformer and condenser reactance are tabulated. These data indicate the relatively small gain in steady-state power limit for even very large sizes of intermediate condenser. Also shown is the relatively small use made of the condenser at the power limit for the larger condenser sizes. Point *A* on the condenser-output curve marks the condenser rating where the output is entirely used at the steady-state power limit, based on transformer reactance X_t plus condenser equivalent reactance X_{ec} equal to 1.0 on rating. For ratings on the left of point *A* the condenser is overloaded to hold normal voltage at the midpoint, whereas for ratings to the right of point *A* the condenser is underloaded when holding this voltage.

Assuming capacitor blocks are switched on as the power transfer is increased, there is one ultimate size of capacitor required at the power limit, still assuming unit voltage held at the midpoint. This limit is shown to be 0.95 per unit, with a total capacitor-bank size of approximately 0.20 per unit. This value of power limit compares with a limit of 0.91 with no intermediate kilovar source and a limit of 1.02 with a condenser whose full rating is used at the power limit.

Steady-state power limits of a double-circuit interconnection under the emergency condition of one line section out for various ratings of intermediate kilovar-source capacity are shown in Figure 12, again comparing the intermediate condenser and capacitor. The kilovar source capacity required at the midpoint is determined by the desired power transfer across the interconnection with one line section out, and the power limit for this initial transfer or the corresponding condenser or capacitor size is shown.

Again the kilovolt-ampere base to use with these data depends upon the line voltage and length under consideration, and Table VI gives examples of bases pertaining to various voltages and line lengths.

In this case, no additional capacitor blocks are added as the power transfer is increased from "initial" to the maximum; hence the voltage drop from normal at midpoint is of interest. This is plotted and shown to be a maximum of about five per cent when no intermediate capacitor is used, tapering to zero when the initial transfer and power limit are equal (zero margin). At this point, of course, the capacitor size for the initial transfer equals that to hold normal midpoint voltage at the power limit.

On the basis of a 20 per cent margin between power transfer and power limit, it may be seen that 0.735 per unit kilowatt may be transmitted using an intermediate capacitor, whereas with a condenser 0.765 per unit kilowatt, or four per cent more, may be transferred.

Summary

It is concluded that for the type of systems studied, the system stability characteristics are affected very little by the type of kilovar supply chosen for a central source. If a system is designed and operated in accordance with well-established principles, utilizing modern relaying, suitable generator-voltage regulators, and switching equipments, the stability problem can be essentially narrowed down and isolated to those cases involving electrically long lines and hydroelectric generating stations. In this study, a wide numerical range of parameters was investigated, allowing certain conclusions to be deduced. The data can likewise be interpreted for other systems of similar types. For instance, the results of a high-reactance hydroelectric generator and transmission line of a given

Table VI. Examples of Kilovolt-Ampere Base Applying to Basic Data in Figures 11 and 12

$$\text{Kilovolt-Ampere Base} = \frac{1{,}722(\text{kv})^2}{\text{Miles of Line}}$$ (see Appendix for Derivation)

Line Voltage, Kilovolts	Line Length, Miles	Kilovolt-Ampere Base
66	100	75,000
	150	50,000
	200	37,500
132	100	300,000
	200	150,000
	300	100,000
220	100	833,000
	200	416,000
	300	277,000

Table VII. Examples of the Various Condenser Ratings Applying to Basic Data in Figure 11 for a Specific Case

A Admittance of Intermediate System	X_{sc} Equivalent Reactance of Condenser on Rating	C Required Condenser Rating, Kva
0.5	1.2	65,000
	0.9	50,000
	0.75	42,500
1.0	1.2	130,000
	0.9	100,000
	0.75	85,000
2.0	1.2	260,000
	0.9	200,000
	0.75	170,000

$C=A(X_t+X_{sc})B$
X_t = intermediate-station transformer reactance = 0.10 per unit on rating
C = rating of intermediate station = rating of condenser in kilvolt-amperes
B = kilovolt-amperes = 100,000
Line length = 300 miles
Voltage = 132 kv

length can be applied with reasonable accuracy to a longer length of line and a lower-reactance generator. Similarly, the results of a heavily loaded station with a large inertia constant can be interpreted for one lightly loaded having a smaller constant. Refer discusssion in appendix on this subject. It follows therefore that if certain general conclusions can be reached for the particular systems studied over a wide range of parameters, the same general conclusions can be applied judiciously to other systems of the same type having different voltages, length of lines, machine ratings, and so forth. With this thought in mind, it will be appreciated that the range of constants and the variations studied make for more of a generalization than would appear superficially.

In view of all this, it appears that the choice should be made on considerations other than stability. Economic considerations generally govern, but before an economic analysis is in order in any event, it is necessary to know the technical facts covering the system performance with the alternative equipments. It is hoped the data offered will aid in this respect.

Discussion of Other Considerations

Voltage Stabilization for Suddenly Applied Loads

Synchronous condensers have been used for reducing voltage flicker of a bus to which fluctuating loads such as arc furnaces were connected.[7] A condenser's inherent characteristic gives it the ability to discourage an attempted fundamental-frequency voltage change at its terminals, while the characteristics of a capacitor are somewhat the opposite. To determine quantitatively this factor for the system studied, the incremental load-voltage transient characteristic was determined on the network analyzer for substation-*A* bus for both a capacitor and a condenser at substation *A*. These results are plotted in Figure 13, for the case of distribution capacitors in the distribution areas. In this instance the difference in bus voltage for a load increment of 20,000 kva at 0.8 power factor between the condenser and capacitor was one per cent or approximately 1.5 per cent for 30,000 kva. The condenser rating, magnitude and power factor of load increment, and the system impedance all obviously affect this difference. And as the major portion of the voltage drop is a result of the incremental load kilovars, a close estimate for other incremental load magnitudes and power factors can be determined from the data calculable from the information in Figure 13. The rating of the condenser or capacitor for the case shown was 50,000 kva.

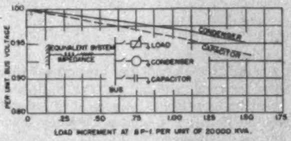

Figure 13. Transient bus voltages for various suddenly applied loads

System Voltage During Swings

As is generally known, capacitors do not have the ability of maintaining system normal-frequency voltage during a sudden circuit change as does a synchronous condenser. This characteristic has little effect on stability limits as determined within the assumptions of this study. However, these voltage characteristics may be important for other reasons, so to visualize better and evaluate the relative voltage conditions obtaining in the load areas during the fault-on period and the subsequent swing for a particular case, the curves in Figure 7 are shown. There curves show the effects of the condenser and capacitor in maintaining voltage during the fault, as well as during the swing.

In a particular instance, the voltages obtaining during a disturbance may or may not be of material significance aside from the stability question so this decision is to be made for the particular case at hand.

Voltage-Regulating Requirements

With no change in field excitation a condenser tends to resist a fundamental-frequency voltage change at its terminals, while a capacitor tends to encourage such a change. If the voltage at the terminals of an automatically controlled condenser falls lower than the lower limit of the "dead band"—be it finite or zero—of the control element, the excitation will increase until the voltage again lies within the dead band. Similarly, an automatically controlled capacitor would be switched in steps until the control is satisfied, but the dead band of the voltage-sensitive master element must be larger than the voltage change caused by the switching on of one step. On large systems automatic switching of capacitors could give relatively smooth voltage control even though it would be controlled in steps with the application of a large quantity of kilovars per step. It is possible that on many systems where voltage regulation is considered of paramount importance, the basic requirement is really kilovar regulation.

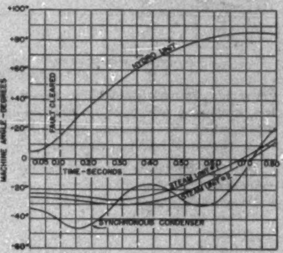

Figure 14. Stable four-machine swing curve used as a basis for combining steam units and reducing system as described in appendix

Other Requirements of High-Voltage Transmission and Integrated Systems

Interconnected systems are generally considered to have kilovar requirements particularly adapted to large synchronous condensers, such as

(*a*). Emergency requirements during temporary loss of a line.

(*b*). Voltage stabilization made necessary by violent load fluctuations.

(*c*). "Lagging" capacity (kilovar absorber as contrasted with a kilovar source) requirements during light-load periods.

Where these requirements are important, the synchronous condenser holds a unique position in the field. On kilovar-source applications requiring severe regulating duty, the multiple-step automatically switched capacitor bank awaits the development of switching elements capable of withstanding many and frequent operations with low maintenance. Such switches already are not beyond the bounds of possibility, and future energy loss and installation cost factors may merit careful consideration and further development of a fast-multiple-step capacitor switching contactor such as the transformer load-ratio-control type.

For applications where the kilovar supply must also act at times as a "kilovar absorber" it is possible to add a shunt reactor of appropriate size and a full-range load-ratio-control transformer. As pointed out previously, however, in the general case of locating a kilovar source near a load and connected to high-voltage lines of an interconnection, the minimum kilovar requirements of the load area may obviate the "lagging" requirements of the condenser or capacitor-reactor combination.

Appendix

Two-Machine Representation of Three-Machine Case Involving Condenser

For the purpose of establishing the accuracy of this method, three- and four-machine swing curves were calculated, the cases representing the large capacitor and the synchronous condenser cases at substation *A*, respectively. The hydroelectric station and the two steam stations constituted the units in the three-machine case and the condenser makes the fourth machine in the four-machine case. A three-phase fault at the sending end of the transmission line was used for all cases, as it represents the most severe condition from a transient-stability standpoint. Figure 14 shows a stable four-machine swing curve for six-cycle switching. The two steam stations swing together for the fault location studied so they are combined, thus reducing the problem to two- and three-machine cases. The critical switching time for a two-machine problem is readily determined by the equal area criterion[8] applied to the torque-angle curve. In the present study this was desirable as the effect of varying the hydroelectric-unit inertia was readily determined from a single set of data, and torque-angle curves are readily determined by the network analyzer for various hydroelectric-unit transient reactance and initial loads, taking into account any assumed load volt-ampere characteristics. The following reasoning allows the resolving of the three-machine case into an equivalent two-machine case for which torque-angle curves can then be taken and critical switch times determined by the equal-area criterion, again allowing a wide range of the parameters to be studied.

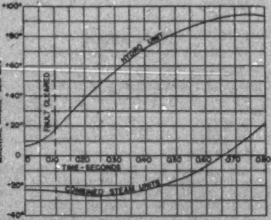

Figure 15. Stable two-machine swing curve for same conditions as Figure 14 except steam units combined and condenser represented by a capacitor in series with a reactance equal to X_d' of condenser, and voltage back of X_d' held constant by varying capacitor

A synchronous condenser can deliver no net energy to the system, else it will fall out of step, and Figure 14 indicates in this case that it has no tendency to lose synchronism. Its primary function is to pump kilovars into the system and maintain voltage. Hence, if the condenser swinging can be neglected (which means neglecting its inertia or adding it to the steam stations), torque-angle curves can be determined by maintaining the condenser voltage back of transient reactance and in phase with the line voltage for various values of parameters. Critical switching times can then be determined from these data for various conditions by the equal-area criterion. This method was used for the data presented in the body of the paper.

The validity of this representation is demonstrated in Figure 15. This swing curve was taken under identical conditions as that of Figure 14, but the steam units were combined back of transient reactance and their inertia constants added and the synchronous condenser was represented as outlined above. A comparison between Figures 14 and 15 indicates the accuracy of the method. At 0.80 second, for example, the angle between the hydroelectric machine and the mean of the two steam units is 70.8 degrees as given by the four-machine swing curves, and is 70.7 degrees for the equivalent representation.

A further demonstration of the soundness of the method was made by making swing curves for the same hydroelectric loading, inertia, and transient reactance with a synchronous condenser and with a capacitor. The torque-angle data analyzed by the equal-area criterion showed the synchronous condenser case to have a critical switching time of 8.7 cycles and the capacitor a time of 9.2 cycles. Swing curves were made for each case with nine-cycle switching. The case with the synchronous condenser was unstable, Figure 16, and that with the capacitor stable, Figure 17, showing that the two-machine representation is very accurate.

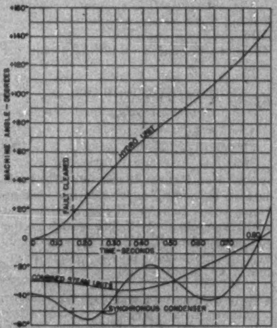

Figure 16. Unstable three-machine swing curve for nine-cycle switching

This is a check on accuracy of data in Figure 3b, for 0.9 load and $X_d' = 0.35$, which shows nine-cycle switching for condenser should be unstable. Refer to appendix for discussion

A Note on Factors Affecting Stability

It can be readily deduced from the discussions and data given in this paper that practically the only difference between a capacitor and a condenser on transient stability results from their different abilities to resist a change in system voltage. And for the particular type of systems studied, these characteristics have a relatively small effect on transient stability. A word of explanation in regard to the factors affecting these results will be given for clarity.

During the fault, the degree to which the load is maintained determines to what degree part of the equivalent generators slow down while they deliver the excess demands made on them resulting from the fault greatly increasing the transfer impedance to other equivalent generators—which speed up. After the fault is removed if sufficient restoring torque is available the two separating groups of machines regain equilibrium. Thus, it can be seen that during the fault if more load is lost—everything else being equal—the system will be more stable than if less load is lost. On the other hand, after the fault is removed, the condenser, by holding a higher voltage in the system between the separating groups of generators, makes for more restoring torque—everything else being equal. The critical switching time is that time which gives equal weight to the forces acting during the fault which tend to separate the generators, and to those after the fault which tend to restore the generators. The weighting of these factors for various conditions represents the results presented in this paper where critical switching times are determined. It follows from this reasoning that the shorter the "fault-on" time, the more favorable the results will be for the condenser. For instance, suppose the power limits were to be determined for zero switching time. The period wherein the capacitor produces results favorable to

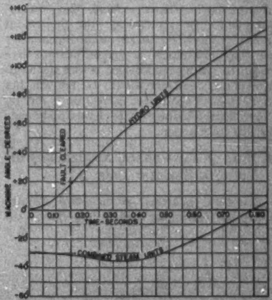

Figure 17. Stable two-machine swing curve for nine-cycle switching

Same condition as Figure 16 except capacitor instead of condenser at substation *A*. Data in Figure 3b indicate this case should be stable. Refer to appendix for discussion stability (the fault-on period) is then nil so the condenser will naturally give higher limits as its favorable period is the only one that exists. Scanning Figures 2 and 3 shows this effect. As the fault-clearing times decrease, the capacitor loses margin over the condenser, and the condenser shows its greatest margin for the case of zero switch time for the capacitor. Refer to Figure 3b for $X_d'=0.45$, $H=6$, and initial load $=1.1$. The greater slowing down of the steam stations during the fault with the condenser than with the capacitor is readily shown by the swing curves of Figures 16 and 17.

In the case of other than three-phase faults, the condenser provides synchronizing torque during the fault, which makes its case more favorable, but at the same time the permissible fault-on period increases, thus favoring the capacitor, so an accurate generalization in respect to unbalanced faults would require further study. Other types of faults, however, will increase the critical switch times in either case, thus reducing the relative seriousness of the stability problem.

A Note on the Effect of Excitation Response of Condensers on System Stability

In this study it was assumed that the voltage back of transient reactance remained constant during the first swing, thus neglecting the regulating system of all synchronous machines. At one time it was felt that exciters with high-speed response improved transient stability by increasing the field flux during the disturbance. This gain proved to be very small for the larger machines under the most favorable circumstances and with the advent of high-speed relays and breakers the gain rapidly approached the vanishing point. Hence, from the transient-stability standpoint, an infinitely variable quick-response regulating system for the condenser is of little moment in this comparison.

In terms of steady-state stability, machine excitation is very important, but load increments for tests of this type of stability are small and it is assumed that the various excitation systems can follow the voltage requirements. Hence, from the steady-state-stability standpoint, the important point is to have excitation on the machines—be they condensers or generators—to give required voltage conditions before the addition of each load increment. Hence, whether the required condenser excitation is put on slow, fast or indifferent is of no importance in this comparison.

A Note on the General Applicability of Network-Analyzer Data

Network analyzer studies are usually made by setting the system impedances up on a convenient kilovolt-ampere base. Results are then obtained measured in per unit of that base. Obviously, the results thus can be interpreted to apply to a system of any size, with component parts of both system and results scaled up or down depending upon the base selected in relation to the base on which the board setup was made. Thus, the results of this, as well as other general studies, can be applied to larger or smaller systems of similar arrangement.

Certain other parameters may also be varied and the base altered accordingly to get different results without changing the analyzer setup. Consider a system of impedance Z supplying power through a line of length l_1, voltage kv_1, and impedance Z_1 ohms per mile. These values are set up in per cent or per unit on a given base B_1. The per-unit line impedance is determined by

$$\text{Per unit } Z_{B1}=\frac{Z_1 l_1 B_1}{(kv_1)^2\,1{,}000} \qquad (1)$$

An expression for the base B_n for any voltage, any value of line impedance in ohms per mile, or any length of line may be had simply by transposing this equation to

$$B_n=\frac{Z_{B1}(kv_n)^2\,1{,}000}{Z_n l_n} \qquad (1a)$$

For the studies reported under "Interconnected Systems," the network-analyzer impedance units were actually set up on the basis of

$B_1=100{,}000$ kva
$Z_1=0.12+j\,0.8$ ohm per mile
$kv_1=132$ kv
$l_1=300$ miles

Putting these values in equation 1, $Z_{B1}=0.206+j1.376$ is obtained.
Therefore, keeping $Z=0.12+j\,0.8$ ohm per mile,

$$B_n=\frac{(0.206+j\,1.376)(kv_n)^2 1{,}000}{(0.2+j\,0.8)\times l_n}$$

$$=1{,}722\,\frac{(kv_n)^2}{l_n} \qquad (2)$$

as shown on Figures 11 and 12. The basic data in these figures for per-unit voltage drops, power limit, condenser size, and so forth, therefore apply to any system on the appropriate base B_n defined by equation 1, with the limitation that the system reactance X be 0.25 in per unit on that base.

A Note on Load Characteristics

In a majority of the stability studies the assumption is made that the load has an impedance characteristic, that is, the kilowatt and kilovar load vary as the square of the voltage. Experience indicates that the error introduced by this assumption is generally acceptable for the usual type of study. Therefore, for practical purposes this assumption should in general be satisfactory also for stability studies wherein a large capacitor bank is being used at substations. However, its validity for comparing small differences that appear to have come out of this study may be questioned. This analysis, therefore, includes an attempt to evaluate the effects on stability of a composite load in the case of the capacitor or the condenser at substation A.

In making a study of this type it is recognized that the transient characteristic of the loads should be used in a rigorous treatment, but little is known of this characteristic for composite systems. Therefore, in view of this shortcoming, the steady-state characteristic of this type of load was used, and from the data on the composite load studied, its characteristic can be readily determined to be such that for a 10 per cent decrease in voltage, the kilowatt and kilovar loads reduce by 12.5 per cent and 14 per cent, respectively. This compares with 13 per cent and 17.4 per cent, respectively, obtained by test[4] for a certain metropolitan load. In view of all this it is felt that the results for the composite-load characteristics obtained should be indicative of what to expect when thinking in terms of using large capacitor banks. As the art advances and more data on composite loads are obtained, this refinement may be made.

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Differential Leakage With Respect to the Fundamental Wave and to the Harmonics

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THE calculation of parasitic torques and forces produced by the harmonics in induction motors and salient-pole synchronous machines is based on exact knowledge of the leakage with respect to the harmonics and especially on the knowledge of the differential leakage.

The standard theories of differential leakage with respect to the synchronous wave fail entirely when applied to the harmonics.

There are two standard theories of differential leakage with respect to the synchronous wave. One treats the differential leakage in a manner similar to the usual treatment of tooth-top leakage. The other has been developed by Professor C. A. Adams.[1] This theory considers for the first time correctly the "air-gap leakage," that is, the harmonic fluxes as the source of the differential leakage. However, C. A. Adams, as well as P. L. Alger[38] who made considerable contributions to Adams' ideas, separates the differential leakage into two parts, the zigzag leakage and belt leakage, "in order to permit the effects of independent variables, number of slots, and number of phase belts to be separately taken into account." This segregation makes Adams' theory inapplicable to the harmonics. Moreover, the first mentioned theory, as well as Adams' theory, do not consider the damping of the harmonic fluxes by the secondary winding although it is evident that any harmonic flux produced by the stator winding will be damped by a squirrel-cage winding in the rotor.

Adams' theory has been taken up by different authors in Europe, especially by L. Dreyfus,[17] M. Baffrey,[23] and M. Krondl.[26] In their investigations the division of the differential leakage in two parts is avoided, but the damping of the harmonic fluxes is still not taken into account. R. Richter[40] considers the damping but gives practical results only for integral-slot full-pitch windings. Further, no due consideration is given to the slot openings. The influence of the slot openings is twofold:

1. The slot openings may considerably decrease or increase the slot harmonics.

2. The slot openings alter the shape of the magnetomotive force curve of the winding so that it is different from the usually assumed stepped shape.

In the following investigation the differential leakage of integral-slot windings as well as of fractional-slot windings will be treated. The influence of the number of slots, of fractional pitch, of skewing, of damping, and of the slot openings will be considered and discussed on practical machines. Formulas and curves will be given which make it possible to calculate the differential leakage in a simple way. In this paper the integral-slot winding and the squirrel-cage winding will be treated.

Preliminary Remarks

Since the stator and rotor carry currents of different frequencies and produce harmonics of different order the harmonic leakage has to be calculated separately for stator and rotor.

Table I applies to a 30-horsepower induction motor with a squirrel-cage rotor, four poles, with 72 slots in the stator and 50 slots in the rotor. The fundamental harmonic is taken as the wave having a length equal to the circumference of the stator bore. This is expedient with respect to the fractional-slot windings where subharmonics may occur. The synchronous wave thus has the order $n'=p$.[41] (For symbols see nomenclature.) The first vertical row represents the stator harmonics up to the slot harmonics of first order. The currents produced by the stator harmonics in the rotor have different frequencies and each stator harmonic produces a row of rotor harmonics (the horizontal rows in Table I).

In the first vertical row, the rotor harmonics have the same orders as the corresponding stator harmonics. These rotor harmonics in co-operation with the stator harmonics produce asynchronous torques in pairs while all other rotor harmonics in co-operation with the stator harmonics produce synchronous cusps.

Each rotor harmonic of the first vertical row is at standstill with respect to the stator harmonic of the same order at *any* speed of the rotor, while the other rotor harmonics are at standstill with respect to the stator harmonics of the same order at a single speed only. Therefore, the first vertical row of the rotor harmonics decreases (damps) the corresponding stator harmonics in the same manner as the synchronous wave of the rotor decreases the synchronous wave of the stator. All other rotor harmonics do not influence the stator harmonics which produce them but they induce harmonic currents in the stator which in turn damp the corresponding rotor harmonics.

As will be shown below, the damping effect of the harmonics with respect to each other is considerable when the secondary is a squirrel-cage winding. A phase-belt winding is able to damp only those harmonics the wave length of which is equal to twice the pole pitch for which the winding is laid out. This is due to the defective interlinkage of a phase-belt winding with the harmonics.

Therefore, there is practically no damping of the stator harmonics in an induction motor with wound rotor and a salient-pole synchronous machine without a damper winding. In the induction motor with a wound rotor each stator harmonic produces a rotor harmonic the half wave length of which is equal to the pole pitch of the machine (see "Examples"). All these rotor harmonics are practically damped out by the stator winding. On the contrary, all stator harmonics are more or less damped in an induction motor with a squirrel-cage rotor and in a salient-pole synchronous machine with a damper winding. Furthermore, in these machines the rotor harmonics whose half wave length is equal to the pole pitch of the machine are damped by the stator but these harmonics are small since they are produced

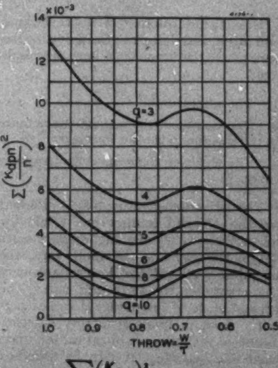

Figure 1. $\sum_{n\neq 1}\left(\frac{K_{dpn}}{n}\right)^2$ for three-phase 60-degree phase-belt windings

For $q=2$, $W/\tau=1.0$, 0.833, 0.667

$$\sum\left(\frac{K_{dpn}}{n}\right)^2=265,\ 205,\ 199\times 10^{-4}$$

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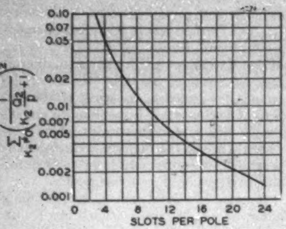


Figure 2. $\sum_{K_{2p}\neq0}\left(\frac{1}{K_2\frac{Q_2}{p}+1}\right)^2$ **for squirrel-cage rotors**

by stator harmonics of high order. There is, therefore, practically no damping of the stator with respect to the rotor in a squirrel-cage motor or in a salient-pole machine with a damper winding.

In the harmonic chart given above, the first horizontal row of rotor harmonics is produced by the rotor current of slip frequency. This row determines the harmonic leakage of the rotor with respect to the synchronous wave. The harmonic leakage of the rotor with respect to the n'th stator harmonic is determined by the horizontal row that corresponds to this harmonic.

The differential leakage reactance of a phase-belt winding (stator winding or wound-rotor winding) will be now determined, at first neglecting the damping.

Differential Leakage Reactance of a Phase-Belt Winding Neglecting the Damping

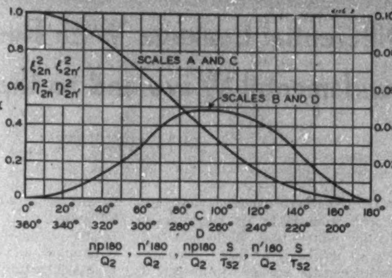
Besides the number of slots, skewing, coil pitch, and damping, the following factors affect the amount of harmonic leakage:

1. The saturation.
2. The change of the magnetic reluctance in the gap due to the slot openings.
3. The shape of the magnetomotive-force curve.

The influence of saturation is different for the different harmonics. While the magnetic path of the higher harmonics lies in the gap and the tooth tops, the magnetic path of the lower harmonics also comprises the teeth and core. The present state of knowledge does not permit a more or less satisfactory determination of the influence of saturation upon harmonic leakage. It can be assumed that this influence is slight.

As to the influence of the slot openings on the harmonic leakage, it is usually assumed that this influence is the same for all harmonics, that is, that all harmonic fluxes will be reduced by the slot openings in the same ratio as the synchronous wave, namely in the ratio 1 to K_c, where K_c is the total Carter factor. For the representation of the magnetomotive-force curve the assumption is usually made that the ampere conductors of each slot are concentrated in the middle of the slot; this leads to the stepped shape of the magnetomotive-force curve. At first, calculations will be made on the basis of these two assumptions.

Figure 3 (right). Coupling and skewing factors for squirrel-cage rotors



The amplitude of the n'th harmonic of the magnetomotive-force curve of the stator is[43]

$$F_{n'}=\frac{\sqrt{2}}{\pi}mN\frac{K_{dpn'}}{n'}I_1 \tag{1}$$

The amplitude of the corresponding field harmonic is

$$B_{n'}=\frac{3.19}{gK_cK_s}\frac{\sqrt{2}}{\pi}mN\frac{K_{dpn'}}{n'}I_1 \tag{2}$$

K_c is the total Carter factor. K_s is an average saturation factor. The flux produced by the n'th field harmonic is

$$\phi_{n'}=\frac{2}{\pi}\frac{p\tau}{n'}B_{n'} \tag{3}$$

and the flux interlinkage due to the flux $\phi_{n'}$

$$\psi_{n'}=\phi_{n'}NK_{dpn'} \tag{4}$$

Thus the differential leakage reactance will be

$$X_d=2\pi f_1\frac{1}{\sqrt{2}I_1}\sum\psi_{n'}\times10^{-8}\text{ ohms}$$

$$X_d=\frac{4}{\pi^2}mf_1N^2\frac{p\tau l_i}{gK_cK_s}10^{-7}\sum_{n'\neq p}\left(\frac{K_{dpn'}}{n'}\right)^2\text{ohms} \tag{5}$$

Since q is an integer equation 5 becomes

$$X_d=\frac{4}{\pi^2}mf_1N^2\frac{\tau l_i}{pgK_cK_s}10^{-7}\sum_{n\neq1}\left(\frac{K_{dpn}}{n}\right)^2\text{ohms} \tag{6}$$

In order to determine the harmonic leakage it is necessary to evaluate the sum $\sum\left(\frac{K_{dpn'}}{n'}\right)^2$. For a three-phase 60-degree phase-belt winding, for example, the summation has to include the harmonics $5p$, $7p$, $11p$, $13p$, and so on.

It is shown in Appendix I that relatively simple expressions can be derived for the sum of equation 5. The derivations are based on the magnetic energy in the gap and on the Goerges vector diagram of the magnetomotive forces. This diagram is a very helpful tool for the investigation of the harmonics. It is often used in Europe, seldom in the United States. The results for three-phase machines with 60-degree phase-belt windings are

$$\sum_{n\neq1}\left(\frac{K_{dpn}}{n}\right)^2=\frac{\pi^2}{18}\frac{(5q^2+1)-\left(\frac{1}{4}\frac{c}{q}+\frac{3}{2}c^2-\frac{1}{4}\frac{c^3}{q}\right)}{3q^2}-K^2_{dp(n=1)} \tag{7}$$

when

$$\frac{2}{3}\leqslant\frac{W}{\tau}\leqslant1$$

and

$$\sum_{n\neq1}\left(\frac{K_{dpn}}{n}\right)^2=\frac{\pi^2}{18}\left\{(5q^2+1)-\frac{\left[3q(c-q)+\frac{2}{3}\frac{c-q}{q}+(c-q)^3-\frac{2}{3}\frac{(c-q)^3}{q}\right]}{4q^2}\right\}-K^2_{dp(n=1)} \tag{8}$$

when

$$\frac{1}{3}\leqslant\frac{W}{\tau}\leqslant\frac{2}{3}$$

c is the chording in slot pitches. W/τ is the width of the coil in pole pitches.

For two-phase windings

$$\sum_{n\neq1}\left(\frac{K_{dpn}}{n}\right)^2=\frac{\pi^2}{16}\frac{(4q^2+2)-\left(\frac{c}{q}+3c^2-\frac{c^3}{q}\right)}{3q^2}-K^2_{dp(n=1)} \tag{9}$$

Equations 7 and 8 are represented in Figure 1. $\Sigma(K_{dpn}/n)^2$ and W/τ are used as co-ordinates, q being the variable. Since the distribution factor K_d decreases with increasing q and the ratio K_{dn}/n

of the slot harmonics also decreases with increasing q, the sum $\Sigma(K_{dn}/n)^2$ decreases considerably with increasing number of slots per pole per phase q. The chording factor K_p decreases the harmonics in a certain range of W/τ, increases then again when W/τ is large. The optimum chording for which the sum $\Sigma(K_{dpn}/n)^2$ becomes a minimum is approximately 0.80.

Equations 7, 8, and 9 are valid not only for a stator winding but for the winding of a round rotor as well.

Differential Leakage Reactance of a Squirrel-Cage Winding With Respect to the Synchronous Wave

Equations 5 and 6 can be applied also to the squirrel-cage winding. In order to get the reactance per bar, introduce the relations

$$m=Q_2 \qquad N={}^1/_2 \qquad K_{dp}=1$$

For the harmonics of a squirrel cage

$$m'=K_2Q_2+n'$$

or

$$m=K_2\frac{Q_2}{p}+n \tag{10}$$

Here only $n'=p$ or $n=1$ has to be considered. Therefore, the harmonic leakage reactance of a squirrel-cage winding for the synchronous wave, per bar, is

$$X_{d(n'=p)}=\frac{1}{\pi^2}f_1Q_2\frac{p\tau l_i}{gK_cK_s}\eta^2{}_{2(n'=p)}10^{-7}\times \sum_{K_2\neq 0}\left(\frac{1}{K_2Q_2+p}\right)^2 \text{ ohms per bar} \tag{11}$$

or

$$X_{d(n=1)}=\frac{1}{\pi^2}f_1Q_2\frac{\tau l_i}{pgK_cK_s}\eta^2{}_{2(n=1)}10^{-7}\times \sum_{K_2\neq 0}\left(\frac{1}{K_2\frac{Q_2}{p}+1}\right)^2 \text{ ohms per bar} \tag{11a}$$

$\eta_{2(n'=p)}=\eta_{2(n=1)}$ is the skew factor for the synchronous wave.

In general,

$$\eta_{2n'}=\frac{\sin n'\frac{\pi}{Q_2}\frac{s}{\tau_{2s}}}{n'\frac{\pi}{Q_2}\frac{s}{\tau_{2s}}} \tag{12}$$

When the rotor is not skewed, η_{2n} is equal to 1.

The sum

$$\sum_{K_2\neq 0}\left(\frac{1}{K_2\frac{Q_2}{p}+1}\right)^2$$

is given in Figure 2 as a function of the number of slots per pole.

In Appendix II it is shown that a very simple expression can be found for the sums in equations 11 and 11a by using equation 56 from Appendix I, which is based on the magnetic energy in the gap and the Goerges vector diagram. These sums become:

$$\sum_{K_2\neq 0}\left(\frac{1}{K_2Q_2+p}\right)^2=\frac{1}{p^2}\left(\frac{1}{\xi^2{}_{2(n'=p)}\eta^2{}_{2(n'=p)}}-1\right) \tag{13}$$

or

$$\sum_{K_2\neq 0}\left(\frac{1}{K_2\frac{Q_2}{p}+1}\right)^2=\frac{1}{\xi^2{}_{2(n=1)}\eta^2{}_{2(n=1)}}-1 \tag{13a}$$

where

$$\xi_{2(n'=p)}=\xi_{2(n=1)}=\frac{\sin\frac{p\pi}{Q_2}}{\frac{p\pi}{Q_2}} \tag{14}$$

is the "coupling factor" of the rotor with respect to the synchronous wave.

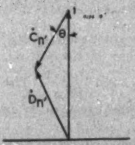


Figure 4. Damping factor of the n'th harmonic

For smaller angles $\left(\frac{p}{Q_2}\leqslant\frac{1}{10}, \frac{p}{Q_2}\frac{s}{\tau_{s2}}\leqslant\frac{1}{10}\right)$, the accuracy of the slide rule is not sufficient for determining the sum from equation 13. For these cases, the following approximation can be used.

$$\sum_{K_2=0}\left(\frac{1}{K_2\frac{Q_2}{p}+1}\right)^2\approx\frac{1}{3}\left[\left(\frac{p\pi}{Q_2}\right)^2+\left(\frac{p\pi}{Q_2}\frac{s}{\tau_{s2}}\right)^2\right] \tag{15}$$

The values of $\xi_{2n}{}^2$ and $\eta_{2n}{}^2$ as functions of the angles p 180/Q_2 and $\left(\frac{p180}{Q_2}\ \frac{s}{\tau_{s2}}\right)$, respectively, are given in Figure 3. The curves of this figure can be used also for the determination of the harmonic leakage reactance of a squirrel-cage winding with respect to the harmonics, as shown in the following section.

The Differential Reactance of a Squirrel-Cage Winding With Respect to the Harmonics

In order to determine the damping effect of the rotor harmonics on the stator harmonics when $K_2=0$ the currents produced by the stator harmonics in the rotor bars must be known. The magnitude of these currents depends upon the reactances and the resistance of the rotor bars, which are different for the different harmonics.

Since the stator harmonics produce currents of high frequency in the rotor, the reduction of the slot leakage and the increase of bar resistance due to the skin effect have to be taken into account. The n'th stator harmonic produces in the rotor a current of the frequency

$$f_{2n'}=f_1\left[1-\frac{n'}{p}(1-s)\right] \tag{16}$$

The differential leakage reactance of the rotor with respect to the harmonics can be determined again from equations 5, 6, and 10 with

$$m=Q_2 \qquad N={}^1/_2 \qquad K_{dp}=1$$

Thus,

$$X_{dn'}=\frac{1}{\pi^2}f_1Q_2\frac{p\tau l_i}{gK_cK_s}\eta_{2n'}{}^210^{-7}\times \sum_{K_2\neq 0}\left(\frac{1}{K_2Q_2+n'}\right)^2 \tag{17}$$

or

$$X_{cn}=\frac{1}{\pi^2}f_1Q_2\frac{\tau l_i}{pgK_cK_s}\eta_{2n}{}^210^{-7}\times \sum_{K_2\neq 0}\left(\frac{1}{K_2\frac{Q_2}{p}+n}\right)^2 \tag{17a}$$

It is shown in Appendix III that

$$\sum_{K_2\neq 0}\left(\frac{1}{K_2Q_2+n'}\right)^2=\frac{1}{n'^2}\left(\frac{1}{\xi_{2n'}{}^2\eta_{2n'}{}^2}-1\right) \tag{18}$$

and

$$\sum_{K_2\neq 0}\left(\frac{1}{K_2\frac{Q_2}{p}+n}\right)^2=\frac{1}{n^2}\left(\frac{1}{\xi_{2n}{}^2\eta_{2n}{}^2}-1\right) \tag{18a}$$

where

$$\xi_{2n'}=\frac{\sin\frac{n'\pi}{Q_2}}{\frac{n'\pi}{Q_2}} \tag{19}$$

and

$$\xi_{2n}=\frac{\sin\frac{np\pi}{Q_2}}{\frac{np\pi}{Q_2}} \tag{20}$$

The leakage coefficient of the n'th harmonic, that is, the ratio between the leakage reactance and the main flux reactance of the n'th harmonic, is as follows:

The main flux reactance of the n'th harmonic is, according to equation 17,

$$X_{mn'}=\frac{1}{\pi^2}f_1Q_2\frac{p\tau l_i}{gK_cK_s}\eta_{2n'}{}^210^{-7}\frac{1}{n'^2} \tag{21}$$

and thus

$$\tau_{2sn'}=\frac{X_{sn'}}{X_{mn'}};\ \tau_{2hn'}=\frac{X_{hn'}}{X_{mn'}};\ \tau_{en'}=\frac{X_{en'}}{X_{mn'}} \tag{22}$$

It follows from equations 17 and 18 that

$$\tau_{2hn'}=\frac{1}{\xi_{2n'}{}^2\eta_{2n'}{}^2}-1 \tag{23}$$

Damping of the Stator Harmonics by a Squirrel-Cage or Damper Winding

The amount of damping of the n'th stator harmonic by the n'th rotor harmonic is equal to the ratio of the resultant magnetomotive force for this harmonic to the magnetomotive force of the stator that produces this harmonic.

The stator magnetomotive force, according to equation 1, is equal to

$$F_{1n'}=\frac{\sqrt{2}}{\pi}m_1N_1\frac{K_{dp1n'}}{n'}I_1 \qquad (24)$$

All quantities will be referred to the rotor. In rotor terms there may be written

$$F_{1n'}=\frac{\sqrt{2}}{\pi}Q_2\frac{1}{2}\frac{1}{n'}\eta_{2n'}I'_{1n'}$$

Thus

$$I'_{1n'}=\frac{2m_1N_1K_{dp1n'}}{\eta_{2n'}Q_2}I_1 \qquad (25)$$

This current $I'_{1n'}$ flowing in the rotor will produce the magnetomotive force determined by equation 1.

The current $I_{2n'}$, which the n'th stator harmonic induces in the rotor bars, can be determined from the voltage equation for the rotor. For any harmonic as well as for the synchronous wave,

$$0=I_{2n'}(r_{2n'}+js_{n'}X_{2n'})+ js_{n'}X_{mn'}(I'_{1n'}+I_{2n'}) \qquad (26)$$

where

$$s_{n'}=1-\frac{n'}{p}(1-s) \qquad (27)$$

$s_{n'}$ is the slip of the rotor with respect to the n'th harmonic. $X_{mn'}$ is the main flux reactance of the n'th harmonic.

$I'_{1n'}$ has, according to equation 25, a definite value for a given load. $I_{2n'}$ is therefore, definitely determined by the load:

$$I_{2n'}=-j\frac{s_{n'}X_{mn'}}{r_{2n'}+js_{n'}X_{mn'}(1+\tau_{2n'})}I'_{1n'} \qquad (28)$$

where, according to equation 22,

$$\tau_{2n'}=\frac{X_{sn'}+X_{hn'}+X_{en'}}{X_{mn'}} \qquad (29)$$

The damping factor for the n'th harmonic is

$$D_{n'}=\frac{I'_{1n'}+I_{2n'}}{I'_{1n'}} \qquad (30)$$

therefore,

$$D_{n'}=1-\frac{js_{n'}X_{mn'}}{r_{2n'}+js_{n'}X_{mn'}(1+\tau_{2n'})} \qquad (31)$$

When $s_{n'}$ is equal to zero, $D_{n'}$ is equal to 1, that is, there is no damping. This was to be expected. When $s_{n'}=0$, the rotor rotates in synchronism with the n' stator harmonic, $I_{2n'}$ is then zero, and only the flux produced by $I'_{1n'}$ exists.

Table I

☐—Slot Harmonic

Stator Harmonics n'	Rotor Harmonics $m'=K_2Q_2+n'$ $K_2=0$	$K_2=+1$	$K_2=-1$	$K_2=+2$	$K_2=-2$
+2	+2	$\boxed{+60}$	$\boxed{-56}$	$\boxed{+118}$	$\boxed{-114}$
−10	−10	+48	−68	+106	−126
+14	+14	+72	−44	+130	−120
−22	−22	+36	−80	+94	−138
+26	+26	+84	−32	+142	−90
−34	−34	+24	−92	+82	−150
+38	+38	+96	−20	+154	−78
−46	−46	+12	−104	+70	−162
+50	+50	+108	−8	+166	−66
−58	−58	0	−116	+58	−174
+62	+62	+120	+4	+178	−54
$\boxed{-70}$	−70	−12	−128	+46	−186
$\boxed{+74}$	+74	+132	+16	+190	−42

When

$$s_{n'}=\infty$$

$$D_{n'}=1-\frac{1}{1+\tau_{2n'}} \qquad (32)$$

This is the smallest possible value of $D_{n'}$. For the different values of the slip $s_{n'}$, $D_{n'}$ changes between 1 and $\frac{\tau_{2n'}}{1+\tau_{2n'}}$. The larger $s_{n'}$, the more the harmonic n' is damped.

When $r_{2n'}$ is negligible as compared with $js_{n'}X_{mn'}(1+\tau_{2n'})$ the damping factor $D_{n'}$ is given by equation 32 at any slip $s_{n'}$. When, furthermore, the slot leakage and the end-winding leakage are negligible as compared with the harmonic leakage, equation 23 is valid for $\tau_{2n'}$ and

$$D_{n'}=1-\xi_{2n'}{}^2\eta_{2n'}{}^2 \qquad (33)$$

Equation 31 can be written (Figure 4)

$$D_{n'}=1-C_{n'}\underline{/\theta} \qquad (34)$$

where

$$C_{n'}=\frac{\sqrt{[(s_{n'}X_{mn'})^2(1+\tau_{2n'})]^2+(s_{n'}X_{mn'}r_{2n'})^2}}{r_{2n'}{}^2+[s_{n'}X_{mn'}(1+\tau_{2n'})]^2} \qquad (35)$$

$$\tan\theta=\frac{r_{2n'}}{s_{n'}X_{mn'}(1+\tau_{2n'})} \qquad (36)$$

For the absolute value of $D_{n'}$

$$D_{n'}=\sqrt{(1-C_{n'}\cos\theta)^2+(C_{n'}\sin\theta)^2} \qquad (37)$$

As will be shown below, the angle θ is usually small for starting as well as for running at normal speed, even for the deep-bar motor with high skin effect. For this reason, only a small error will be made when equation 37 instead of equation 34 is used for calculating the damping.

As the foregoing considerations of this section show, equation 37 relates to a certain stator harmonic, namely, the n'th harmonic. In order to get the total damping of the stator harmonic leakage by a squirrel-cage winding, it is necessary to multiply all harmonics of the stator by their corresponding damping factor $D_{n'}$. Equation 5 becomes

$$X_d=\frac{4}{\pi^2}mf_1N^2\frac{prl_i}{gK_cK_s}10^{-7}\times \sum_{n'\neq p}\left(\frac{K_{dpn'}}{n'}\right)^2D_{n'}\text{ ohms} \qquad (38)$$

Thus, the total damping factor is

$$D_t=\frac{\sum\limits_{n'\neq p}\left(\frac{K_{dpn'}}{n'}\right)^2D_{n'}}{\sum\limits_{n'\neq p}\left(\frac{K_{dpn'}}{n'}\right)^2} \qquad (39)$$

or, for the integral slot winding,

$$D_t=\frac{\sum\limits_{n\neq 1}\left(\frac{K_{dpn}}{n}\right)^2D_{n}}{\sum\limits_{n\neq 1}\left(\frac{K_{dpn}}{n}\right)^2} \qquad (40)$$

$D_{n'}$ depends upon the constants of the rotor winding and on the slip $s_{n'}$. $D_{n'}$ is different for the different harmonics n'; it is relatively small for the harmonics of lower order (5th, 7th, 11th) and high, in the limits just given, for the higher harmonics, especially for the slot harmonics. As will be seen from examples discussed hereinafter, $D_{n'}(D_n)$ depends mainly upon the number of slots of the rotor and on the skew.

$K_{dpn'}$ depends upon the stator winding only, that is, upon its number of slots per pole per phase and upon its coil pitch.

Thus, the resultant damping factor depends mainly upon the number of slots of the stator, the coil pitch of the stator, the number of slots of the rotor, and the skew.

Examples

Some examples will now be considered, in order to study the influence of the rotor resistance and the three parts of the rotor reactance on the damping.

First Example

Induction motor with squirrel-cage rotor for 400 horsepower, 60 cycles, three phases; $p=6$, $Q_1=180$, $Q_2=142$, $W/\tau=0.934$. The cage has shallow bars, not skewed.

The motor will be considered at standstill, $s=1$, and at the normal slip $s=0.018$. According to equation 27, $s_{n'}$ is at standstill for all harmonics equal to 1.

The number of pole pairs of the stator-slot harmonics of first order is

$$n_{sl.1}=\mp\frac{Q_1}{p}+1=\begin{matrix}-29\\+31\end{matrix}$$

Table II gives the values of the rotor constants as well as of $C_{n'}$, $\tan\theta$, and $D_{n'}$ for all harmonics up to the 31st. Since the rotor has shallow bars, the resistance $r_{n'}$ (column 3) changes very little with the order of the harmonics. The same is true of the sum of the slot leakage reactance and end-winding leakage reactance, $X_{(s+e)n'}$ (column 4), but the leakage coefficient $\tau_{(s+e)n'}$ (column 5) increases with increasing n' because the main-flux reactance of the harmonics decreases rapidly with the order of the harmonics (column 2). The same is true of the leakage coefficient $\tau_{hn'}$ (column 7) because the coupling factor $\xi_{n'}$ decreases with the increasing order of the harmonic (see Figure 3 and equation 23).

The angle θ is small for all harmonics (column 9). $C_{n'}$ decreases rapidly with increasing n' (column 10). Therefore, the damping factor $D_{n'}$ increases with increasing order of the harmonics (column 11).

The smaller the value of $D_{n'}$, the more the corresponding stator harmonic n' is damped. It is seen from column 11 that the higher harmonics, especially the slot harmonics, are damped very little, while the lower harmonics (the fifth and seventh) are damped very considerably.

Column 12 is calculated by using equation 32, that is, neglecting $r_{n'}$; column 13 is calculated by using equation 33, that is, neglecting $r_{n'}$ as well as $X_{(s+e)n'}$. It is seen from column 12 that the resistance has practically no influence on the damping factor. Column 13 shows that the slot leakage and the end-winding leakage of the rotor cannot be neglected, because this would give too much damping, especially for the lower harmonics. To neglect these reactances is equivalent to an increase of the rotor current and, therefore, to an increase of the damping. It should be noted, however, that the influence of $X_{(s+e)n'}$ is especially large in this machine, for the extension of the rotor bars beyond the core is here large and the skin effect negligible because of the shallow bars.

Evaluation of equation 40 gives for the total damping in this case

$D_t=0.81$

Table II relates to standstill ($S_{n'}=1$). For the normal slip ($s=0.018$) Table III applies. $s_{n'}$ is here different for the different harmonics. The leakage coefficient τ_{hn} is independent of the slip; columns 3 and 7 are, therefore, the same as for Table II.

Due to the high frequency of the rotor currents, the resistance r_n is much larger than in Table II. Yet a comparison between column 11 and column 12 shows that here too, the resistance has no influence on the damping. The increase of r_n is accompanied by a decrease of X_{mn}; the values of $X_{(s+e)n}$ (column 5) are therefore smaller than in Table II. The damping factors D_n are slightly smaller for all harmonics than in Table II. The evaluation of equation 40 gives the total damping factor

$D_t=0.80$

This is practically the same value as for standstill.

With respect to equation 40, the following may be noted: The denominator of this equation can be taken from Figure 1, where the sums include *all* harmonics except $n=1$. Evaluating the numerator of equation 40, it would not be sufficient to take into account the lower harmonics up to the slot harmonics of the first order. Thus, Tables II and III do not suffice for the determination of the numerator of equation 40. Satisfactory results will be usually obtained, if the lower harmonics up to the 19th and *all* slot harmonics are included in the sum $\sum\limits_{n\neq1}\left(\frac{K_{dpn}}{n}\right)^2 D_n$. It is shown in Appendix IV that for all slot harmonics, except the slot harmonics of first order,

$$\left[\sum\left(\frac{K_{dpn}}{n}\right)^2 D_n\right]_{n=n_{sl}\text{(except first order)}}=K_{dp1}^2\left[\frac{1}{(-4mq+1)^2}+\frac{1}{(4mq+1)^2}+\frac{0.786}{(2mq)^2}\right] \quad (41)$$

Table II

Standstill

1 n	2 s_n	3 X_{mn} ($\times10^{-4}$)	4 r_n ($\times10^{-4}$)	5 $X_{(s+e)n}$ ($\times10^{-4}$)	6 $\tau_{(s+e)n}$	7 τ_{hn}	8 τ_{2n}	9 $\tan\theta$	10 C_n	11 D_n	12 D_n for $r_n=0$	13 D_n for $r_n=0$ and $X_{(s+e)n}=0$
− 5	1	6.84	968	3.72	0.494	0.153	0.647	0.086	0.612	0.39	0.39	0.184
+ 7	1	6.49	0.966	3.38	0.965	0.332	1.297	0.121	0.433	0.574	0.564	0.25
−11	1	1.415	0.965	3.37	2.38	1.126	3.51	0.147	0.207	0.805	0.778	0.53
+13	1	1.016	0.965	3.37	3.34	2.05	5.39	0.149	0.155	0.85	0.843	0.672
−17	1	0.591	0.966	3.37	5.7	7.96	13.66	0.111	0.063	0.94	0.932	0.887
+19	1	0.474	0.968	3.38	7.14	16.88	24.0	0.082	0.040	0.96	0.96	0.942
−29	1	0.204	0.967	3.38	16.58	33.5	50.1	0.092	0.0194	0.98	0.98	0.98
+31	1	0.178	0.066	3.37	18.92	23.4	42.3	0.11	0.0176	0.98	0.98	0.96

Table III

Normal Slip

1 n	2 s_n	3 X_{mn} ($\times10^{-4}$)	4 r_n ($\times10^{-4}$)	5 $X_{(s+e)n}$ ($\times10^{-4}$)	6 $\tau_{(s+e)n}$	7 τ_{hn}	8 τ_{2n}	9 $\tan\theta$	10 C_n	11 D_n	12 D_n for $r_n=0$	13 D_n for $r_n=0$ and $X_{(s+e)n}=0$
− 5	+ 5.91	6.84	2.22	2.99	0.437	0.153	0.696	0.036	0.629	0.371	0.37	0.134
+ 7	− 5.88	3.49	2.21	2.99	0.857	0.332	1.189	0.049	0.460	0.54	0.54	0.25
−11	+11.8	1.415	3.05	2.88	2.03	1.126	3.16	0.045	0.242	0.757	0.757	0.53
+13	−11.75	1.01	3.05	2.89	2.86	2.05	4.91	0.043	0.169	0.831	0.83	0.672
−17	+17.7	0.591	3.68	2.83	4.79	7.96	12.75	0.025	0.073	0.927	0.927	0.887
+19	−17.65	0.474	3.68	2.84	6.0	16.88	22.88	0.019	0.046	0.954	0.95	0.942
−29	+29.45	0.204	4.68	2.80	13.74	33.5	47.2	0.005	0.021	0.996	0.996	0.98
+31	−29.45	0.178	4.68	2.79	15.7	23.4	39.1	0.022	0.025	0.975	0.975	0.96

SECOND EXAMPLE

Induction motor with squirrel-cage rotor for 500 horsepower, 60 cycles, three phases; $p=3$, $Q_1=90$, $Q_2=74$, $W/\tau=0.80$. The cage has deep bars, not skewed.

Tables IV and V give the values for this motor, for standstill as well as for normal slip ($s=0.012$). The stator slot harmonics of first order are -29 and $+31$. A comparison between the columns 11 and 12 shows that, despite the high skin effect due to the deep bars, the rotor resistance r_n has little influence on the damping. A comparison between the columns 13 and 11 shows the same as in the first example: the influence of the slot leakage and end-winding leakage of the rotor on the damping is considerable. As in the first example, the damping is practically the same as standstill and at normal slip.

The evaluation of equation 40 gives the total damping factor at standstill

$D_t=0.92$

and at normal speed of the motor

$D_t=0.91$

THIRD EXAMPLE

Induction motor with squirrel-cage rotor for 20 horsepower, 60 cycles, three phases; $p=1$, $Q_1=24$, $Q_2=31$, $W/\tau=0.50$. The cage has shallow bars of cast aluminum.

Tables VI, VII, VIII, and IX give the values for this motor, the first two tables for the skewed rotor and the latter two tables for the nonskewed rotor. The stator-slot harmonics of first order are here -23 and $+25$.

With respect to the influence on the damping of the resistance, the slot leakage, and end-winding leakage of the rotor, the results are the same as in the foregoing example. A comparison between Tables VI and VII and VIII and IX shows the influence of the skewing. The skewing decreases the main-flux reactance, increases the harmonic leakage (see equation 18), and thus increases considerably the total leakage coefficient τ_{2n}. An increase of D_n, that is, a decrease of the damping, is the result. That the skewing reduces the damping can be seen directly from the approximate formula, equation 33. The evaluation of equation 40 gives the total damping factor

$D_t=0.80$

for the skewed rotor, and

$D_t=0.73$

for the nonskewed rotor.

Resultant Damping Factor

It has been mentioned that the resultant damping factor depends mainly upon the number of stator slots, the coil pitch of the stator winding, the number of rotor slots, and the skew of the rotor.

Figure 5 shows the dependence of the damping factor on the number of slots of the stator and rotor for three-phase machines. This part of the total damping factor is denoted by D_Q.

For the same number of rotor slots, D_Q decreases with increasing q_1. As has been seen from the examples, the lower harmonics are more strongly damped than the slot harmonics. Since the order of the slot harmonics is higher for higher values of q_1, and thus the slot harmonics contribute less when q_1 is larger, D_Q must be smaller for higher values of q_1. When q_1 is equal to two or three, the slot harmonics are damped considerably; this explains the intersection of the curves for $q_1=2$ and $q_1=3$ with the curve for $q_1=4$.

For the same number of slots in the stator, D_Q decreases with increasing Q_2/p, because ξ_{sn}^2 increases with increasing Q_2/p (see Figure 3 and equation 33).

The influence of the chording of the stator winding, for three-phase machines, is given by the factor D_C in Table X. The chording reduces the ratio $(K_{dpn}/n)^2$ of the lower harmonics more than that of the slot harmonics. $\Sigma(K_{dpn}/n)^2$ has its lowest value at approximately $W/\tau=0.80$ and increases with larger throw. Since the chording affects the lower harmonics more than the higher ones, and since D_n is smaller for the lower harmonics than for the higher harmonics, D_t as a function of W/τ will have its maximum at a throw approximately 80 per cent (same Q_2 presumed), and thus the factor D_C, by which D_Q has to be multiplied in order to take into account the chording, will be larger for the higher values of q_1. For the chording $^2/_3$ and $^1/_2$ the factor D_C is equal to 1, because K_{pn} is the same for all harmonics (0.866 and 0.707, respectively).

The influence of the skewing is given by Figure 6, where it is assumed that the

Table IV

Standstill

1 n	2 s_n	3 X_{mn} ($\times10^{-4}$)	4 r_n ($\times10^{-4}$)	5 $X_{(s+e)n}$ ($\times10^{-4}$)	6 $\tau_{(s+e)n}$	7 τ_{bn}	8 τ_{2n}	9 $\tan\theta$	10 C_n	11 D_n	12 D_n for $r_n=0$	13 D_n for $r_n=0$ and $X_{(s+e)n}=0$
−5	1	11.5	1.133	2.79	0.243	0.14	0.383	0.074	0.726	0.28	0.277	0.122
+7	1	5.85	1.132	2.78	0.475	0.305	0.78	0.109	0.56	0.45	0.438	0.234
−11	1	2.37	1.131	2.78	1.172	1.0	2.17	0.152	0.312	0.695	0.684	0.50
+13	1	1.70	1.130	2.78	1.635	1.762	3.4	0.151	0.226	0.78	0.773	0.638
−17	1	0.993	1.131	2.78	2.80	6.04	8.84	0.117	0.101	0.90	0.898	0.858
+19	1	0.795	1.131	2.79	3.50	12.33	15.83	0.084	0.059	0.935	0.935	0.925
−29	1	0.341	1.133	2.80	8.2	49.5	57.7	0.049	0.0128	0.987	0.987	0.98
+31	1	0.299	1.131	2.78	9.3	28.4	37.7	0.094	0.024	0.976	0.976	0.966

Table V

Normal Slip

1 n	2 s_n	3 X_{mn} ($\times10^{-4}$)	4 r_n ($\times10^{-4}$)	5 $X_{(s+e)n}$ ($\times10^{-4}$)	6 $\tau_{(s+e)n}$	7 τ_{bn}	8 τ_{2n}	9 $\tan\theta$	10 C_n	11 D_n	12 D_n for $r_n=0$	13 D_n for $r_n=0$ and $X_{(s+e)n}=0$
−5	+5.94	11.5	2.51	1.965	0.171	0.14	0.311	0.028	0.763	0.239	0.238	0.122
+7	−5.92	5.85	2.51	1.95	0.334	0.305	0.639	0.044	0.61	0.392	0.389	0.234
−11	+11.88	2.37	3.48	1.70	0.718	1.0	1.72	0.045	0.37	0.625	0.625	0.50
+13	−11.84	1.70	3.48	1.70	1.0	1.762	2.76	0.046	0.27	0.728	0.728	0.638
−17	+17.8	0.993	4.23	1.60	1.62	6.04	7.66	0.028	0.116	0.885	0.885	0.858
+19	−17.8	0.795	4.23	1.61	2.03	12.33	14.36	0.0195	0.065	0.939	0.935	0.925
−29	+29.6	0.341	5.41	1.478	4.33	49.5	53.8	0.0098	0.0182	0.984	0.984	0.98
+31	−29.6	0.299	5.41	1.46	4.88	28.4	33.3	0.0179	0.029	0.971	0.971	0.966

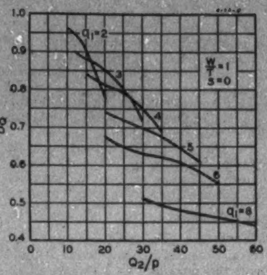


Figure 5. Influence of the number of stator and rotor slots on the damping of the differential leakage of three-phase machines

skew of the rotor is equal to a stator-slot pitch. The skew factor η_{sm} is a multiplicand of the coupling factor ξ_{sm} (equation 33) and has the same influence on the damping as ξ_{sm}. The larger the skewing the larger D_s and D_t.

The total damping factor is

$$D_t = D_Q D_C D_S \tag{42}$$

Figures 5 and 6 and Table X are average values based on calculations of many machines.

Damping of the Stator Harmonics by a Wound Rotor

In order to find the influence of a wound rotor upon the stator harmonics, example 3 of the foregoing section will be considered again, with a wound rotor having $Q_2 = 30$ slots, two coil sides per slot, one conductor per coil side, and a throw equal to 1. The rotor is assumed not to be skewed.

For the harmonics of a wound rotor

$$m' = K_2 p m_2 + n'$$

or

$$m = K_2 m_2 + n \tag{43}$$

A normal three-phase 60-degree phase-belt winding is assumed. This winding does not produce even harmonics, that is, K_2 is a positive or negative even integer. The harmonics chart is given in Table XI.

The table shows that all stator harmonics produce rotor harmonics of the same order.

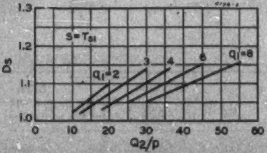


Figure 6. Influence of skewing on the damping of the differential leakage of three-phase machines

The main harmonic is of course different for the different harmonics; it has the order of the stator harmonic, namely n. On the other hand, the wave $n=1$ has to be considered for the harmonics as a parasitic harmonic that produces a harmonic-leakage drop. This rotor wave $n=1$ will be considerably damped by the stator winding because the stator winding is laid out for the pole pitch of this wave. Neglecting the resistance of the stator winding, the damping factor will be $\tau_1/(1+\tau_1)$.

Since all horizontal rows of the chart of the rotor harmonics contain the same harmonics, the harmonic leakage reactance can be calculated from the first horizontal line, that is, from $X_{h(n=1)}$ when the reactance of the main harmonic is subtracted from this value and the reactance of the wave $n=1$ is added. Thus,

$$X_{hn} = X_{h(n=1)} - X_{mn} + \frac{\tau_1}{1+\tau_1} X_{m(n=1)} \tag{44}$$

and

$$\tau_{hn} = \frac{X_{hn}}{X_{mn}} = \left[\tau_{h(n=1)} + \frac{\tau_1}{1+\tau_1}\right] \frac{X_{m(n=1)}}{X_{mn}} - 1 \tag{45}$$

According to equation 5a

$$X_{m(n=1)} = \frac{4}{\pi^2} m_1 f_1 N_1^2 \frac{\tau l_i}{p g K_c K_s} 10^{-7} (K_{dp(n=1)} \eta_{(n=1)})^2 \tag{46}$$

and

$$X_{mn} = \frac{4}{\pi^2} m_1 f_1 N_1^2 \frac{\tau l_i}{p g K_c K_s} 10^{-7} \left(\frac{K_{dpn} \eta_n}{n}\right)^2 \tag{47}$$

By inserting equations 46 and 47 in equation 45 there results

$$\tau_{hn} = \left[\tau_{h(n=1)} + \frac{\tau_1}{1+\tau_1}\right] \times \left(\frac{K_{dp(n=1)} \eta_{(n=1)}}{K_{dpn} \eta_n} n\right)^2 - 1 \tag{48}$$

The leakage coefficient covering the slot leakage and the end-winding leakage, for the nth harmonic, is

$$\tau_{(s+e)n} = \frac{X_{sn} + X_e}{X_{mn}} \tag{49}$$

X_{sn} is different for the different harmonics due to the skin effect. X_e can be assumed as being the same for all harmonics.

When the stator has fractional-slot winding, equations 48 and 49 become

$$\tau_{hn'} = \left[\tau_{h(n'=p)} + \frac{\tau_1}{1+\tau_1}\right] \times \left(\frac{K_{dp(n'=p)}}{K_{dpn'}} \cdot \frac{\eta_{(n'=p)}}{\eta_{n'}} \cdot \frac{n'}{p}\right)^2 - 1 \tag{50}$$

$$\tau_{(s+e)n'} = \frac{X_{sn'} + X_e}{X_{mn'}} \tag{51}$$

The results, for the 20-horsepower motor (example 3 from the foregoing section) with a wound rotor are given in Table XII. In this, the rotor resistance and the influence of the skin effect on the rotor-slot leakage are neglected.

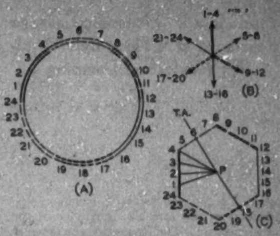


Figure 7. Goerges diagram for a two-pole three-phase winding with four slots per pole per phase

Winding not chorded
q = 4
Two poles, three phases

A comparison between Table XII and Table VIII, which relates to the same 20-horsepower motor with a squirrel-cage rotor, shows that the damping effect of a wound rotor is negligible. The same is true for the stator with respect to the rotor harmonics.

A phase-belt winding damps considerably only that harmonic the half wave length of which is equal to the pole pitch for which the winding is laid out, that is, the wave $n=1$ $(n'=p)$. The negligible influence of a phase-belt winding on the harmonics, as compared with that of a squirrel-cage winding, is due to the small values of K_{dpn} for a phase-belt winding (see Table XII). For a squirrel cage rotor, $K_{dpn}=1$. The small values of K_{dpn} make the values of X_{mn} small and, therefore, the values of τ_{hn} large.

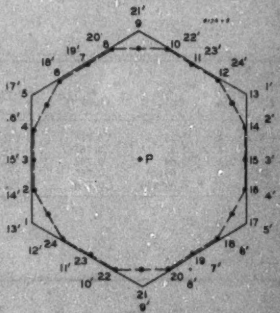


Figure 8. Same as Figure 7

Winding chorded two slot pitches (throw = 0.833)
q = 4
Two poles, three phases

Appendix I. Harmonic Leakage Reactance of a Phase-Belt Winding Neglecting Damping

For the magnetic energy of an m-phase winding,

$$E_m=\frac{m}{2}LI^2 \qquad (52)$$

where L is the inductance of one phase (self-inductance plus mutual inductance of the other phases). The magnetic reluctance of the iron is assumed to be equal to zero and therefore the gap only will be considered. For the magnetic energy of the gap

$$E_m=\frac{1}{2}\int_s hb\,ds=\frac{1}{2\times0.4\pi}\int_s b^2ds \qquad (53)$$

where b is the flux density in the gap, and ds is an element of the space between stator and rotor. Combining equation 53 with equations 52, 1, and 2, it follows, for the reactance that corresponds to the inductance L,

$$X_g=2\pi f_1L10^{-8}$$
$$=\frac{2\pi f_1}{0.4\pi\times2.54}\left(\frac{3.19}{gK_c}\right)^2\frac{10^{-8}}{mI^2}\int_s f^2ds \text{ ohms} \qquad (54)$$

Assuming that the magnetomotive force curve has a stepped shape, the magnetomotive force is constant over each slot pitch, changing its value from slot pitch to slot pitch. This makes it possible to divide the space s into as many elements as there are slots in the machine part under consideration (stator or rotor). Thus

$$ds=\frac{2p\tau l_t gK_c}{Q} \qquad (55)$$

and

$$X_g=4.06\pi^2f_1\frac{p\tau l_t}{gK_c}\frac{1}{mI^2}10^{-8}\sum_Q\frac{f_t^2}{Q} \qquad (56)$$

The magnetomotive force of one slot is equal to $\sqrt{2}In_c$. Introducing this value and also $N=pqn_c$ in equation 56 there results

$$X_g=4.06\times2\pi^2\frac{1}{m}f_1\frac{N^2}{q^2}\frac{\tau l_t}{pgK_c}10^{-8}\times \sum_Q\frac{1}{Q}\left(\frac{f_t}{\sqrt{2}In_c}\right)^2 \qquad (57)$$

Since the total magnetic energy in the gap has been considered, X_g represents the total reactance, that is, the reactance due to the harmonics as well as to the synchronous wave.

Comparing equation 57 with equation 5a, which represents the reactance due to the harmonics only, there is

$$\sum_{n\neq1}\left(\frac{K_{dpn}}{n}\right)^2+K^2_{dp(n=1)} =\frac{2\pi^2}{m^2q^2}\sum_Q\frac{1}{Q}\left(\frac{f_t}{\sqrt{2}In_c}\right)^2$$

or

$$\sum_{n\neq1}\left(\frac{K_{dpn}}{n}\right)^2 =\frac{2\pi^2}{m^2q^2}\sum_Q\frac{1}{Q}\left(\frac{f_t}{\sqrt{2}In_c}\right)^2-K^2_{dp(n=1)} \qquad (58)$$

The computation of the sum $\Sigma(K_{dpn}/n)^2$ is thus reduced to the computation of the sum

$$\sum_Q\frac{1}{Q}\left(\frac{f_t}{\sqrt{2}In_c}\right)^2$$

where f_t is the magneto- motive force over the different teeth of the machine part under consideration. The magnetic energy in the gap of an m-phase machine is independent of time; it is the same at any instant. However, the magnetomotive forces f_t over the different teeth depend upon the instant chosen. Yet, since the magnetic energy is constant, the sum

$$\sum_Q\frac{1}{Q}\left(\frac{f_t}{\sqrt{2}In_c}\right)^2$$

is also constant, irrespective of time.

The value of the magnetomotive force over the different teeth can be found easily by using Goerges' vector diagram of the magnetomotive forces. It offers the advantage of giving a graphical representation of the magnetomotive forces over the different teeth, from which the magnitude of the harmonics, the influence of chording on the harmonics, and so forth, can be visualized.

The Goerges diagram is based upon the following consideration. If A denotes the ampere conductors per unit circumference, then the magnetomotive force at a point x is

$$f(x)=\int Adx+C \qquad (59)$$

When A is a sinusoidal function of time, $f(x)$ can be represented as a vector. Figure 7 shows this procedure for a two-pole three-phase winding with four slots per pole per phase. It is assumed that the winding is not chorded. In this case the upper and the lower layers lie in the same slots. Figure 7A shows the distribution of the 24 slots between the layers and phases. Figure 7 B shows the directions of the currents in the different slot-groups. Figure 7C shows the graphical integration. Since the slots 1, 2, 3, and 4 carry currents of the same direction, their vectors lie in the same line 1–4; the vectors which correspond to the slots 5 to 8 also lie in the same direction, but they are shifted with respect to 1–4 by an angle of 60 degrees according to Figure 7B, and so forth. The polygon closes after each two poles, when q is an integer.

The polygon represents the integral $\int Adx$. In order to find the magnetomotive forces over the different teeth 1, 2, 3 . . . ., it is necessary to determine the constant c, that is, to find the pole of the polygon 1. If this pole has been found to be the point* P (Figure 7C); then the distances $P1$, $P2$, $P3$ . . . are the amplitudes of the magnetomotive forces at the tooth centers 1, 2, 3 . . . . It is assumed that the centers of the slots coincide with the centers of the distances 1–2, 2–3 . . . and that, at these points, the

Table VI

Standstill, Rotor Skewed

n	s_n	X_{mn} ($\times10^{-4}$)	r_n ($\times10^{-4}$)	$X_{(s+e)n}$ ($\times10^{-4}$)	$\tau_{(s+e)n}$	τ_{hn}	τ_{2n}	tan θ	C_n	D_n	D_n for $r_{n=0}$	D_n for $r_{n=0}$ and $X_{(s+e)n=0}$
− 5	1	7.25	0.776	0.737	0.102	0.256	0.358	0.0789	0.74	0.266	0.264	0.204
+ 7	1	3.2	0.767	0.719	0.225	0.563	0.788	0.134	0.555	0.456	0.441	0.36
−11	1	0.84	0.762	0.708	0.843	2.16	3.0	0.227	0.245	0.765	0.75	0.684
−23	1	0.00136	0.765	0.715	526	3,000	3,530	0	0.00028	1	1	1
+25	1	0.000245	0.770	0.725	2,960	26,600	29,600	0	0.000034	1	1	1

Table VII

Normal Slip, Rotor Skewed

n	s_n	X_{mn} ($\times10^{-4}$)	r_n ($\times10^{-4}$)	$X_{(s+e)n}$ ($\times10^{-4}$)	$\tau_{(s+e)n}$	τ_{hn}	τ_{2n}	tan θ	C_n	D_n	D_n for $r_{n=0}$	D_n for $r_{n=0}$ and $X_{(s+e)n=0}$
− 5	+ 5.87	7.25	1.455	1.05	0.145	0.256	0.401	0.024	0.711	0.29	0.286	0.204
+ 7	− 5.83	3.2	1.446	1.03	0.322	0.563	0.885	0.041	0.527	0.474	0.469	0.36
−11	+11.72	0.84	2.25	0.932	1.11	2.16	3.27	0.0396	0.233	0.77	0.766	0.684
−23	+23.4	0.00136	3.19	0.518	381	3,000	3,380	0	0.000295	1	1	1
+25	−23.4	0.000245	3.19	0.528	2,150	26,600	28,700	0	0.000034	1	1	1

---

* If the magnetomotive force $f(x)$ is represented as a function of x in rectangular co-ordinates, c will be determined by the fact that the positive and negative areas of $f(x)$ must be equal.

magnetomotive force increases step by step, each being equal to $\sqrt{2}In_c$; the points 1, 2, 3 are then the centers of the teeth. The instantaneous values of the magnetomotive forces over the different teeth can be found by projecting the vectors $P1$, $P2$, $P3$ . . . on the time line. The shape of the magnetomotive force of a three-phase system changes each one-sixth of a period and repeats itself every one-third of a period. This condition can be satisfied by the vector diagram, Figure 7C, only when the pole P lies in the center of gravity of the polygon.

Referring again to equation 58, take the sum of the squares of the projections of all vectors $P1$, $P2$, $P3$ . . . on the time line. The position of the time line can be chosen arbitrarily, since the magnetic energy in the gap is constant. If we make the unit of length in the vector diagram, Figure 7C, equal to $\sqrt{2}In_c$, the amplitudes of the magnetomotive forces of the different teeth, as well as the instantaneous values of the magnetomotive forces, will be expressed per unit.

In Figure 7C the position of the time line is assumed, as indicated, passing through the points 6 and 18. The polygon is symmetrical with respect to the time axis and, therefore, only one half of the polygon needs to be considered. The projections of the vectors 06 and 017 are equal, their value is equal to 4 cos 30 degrees, and the sum of their squares is equal to $(q-1)q^2 \times 3/4$. Considering the vector 8–12 the projection of 08 is equal to 06, the projection of 09 is equal to three-fourths of 06, the projection of 010 is equal to $^1/_2$ of 06, and so on. Thus, the sum of their squares, taken twice, is

$$2\cos^2 30\,[q^2+(q-1)^2+(q-2)^2 \ldots +1^2] = \frac{3}{2}\,\frac{q(q+1)(2q+1)}{6}$$

Therefore

$$\sum_{Q}\frac{1}{Q}\left(\frac{f_t}{\sqrt{2}In_c}\right)^2 = \frac{1}{12}(5q^2+1)$$

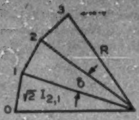


Figure 9. Goerges diagram for a squirrel-cage winding

and

$$\sum_{n\neq 1}\left(\frac{K_{dpn}}{n}\right)^2 = \frac{\pi^2}{18}\,\frac{5q^2+1}{3q^2} \qquad (60)$$

corresponding to equation 7 for $c=0$.

The difference between the vector $P1$, $P2$ . . . of the vector diagram, Figure 7C, is a measure of the amplitudes of the harmonics. If there were no harmonics, the vector diagram would be a circle. The radius of the synchronous wave per unit is

$$R_s = \frac{0.45mIn_c qK_{dp(n=1)}}{\sqrt{2}In_c} = \frac{m}{\pi}\,qK_{dp(n=1)} \qquad (61)$$

Figure 7 relates to a nonchorded winding where the upper layer and lower layer coincide with respect to space. When the winding is chorded, that is, the lower layer is shifted with regard to the upper layer, the Goerges' vector diagram can be found in the following way:

1. The vector diagram is to be drawn for the upper layer as shown hereinbefore.

2. The sequence of the slots for two poles and for both layers is to be set up according to the chording chosen.

3. The vector diagram is to be drawn for the lower layer so that the diagram pole P is the same as for the upper layer, that is, the vector $P1'$ must be equal to the vector $P1$, the vector $P2$ must be equal to the vector $P2'$, and so on (prime indicates the lower layer). Starting with the slot 1' the vector diagram of the lower layer is to be drawn in opposite direction to that of the upper layer.

4. The end points of both vector diagrams are to be connected corresponding to the distribution of both layers in the slots, that is, each pair of coil sides that lie in the same slot is to be connected by a vector. The centers of these new vectors represent the end points of the resultant vector diagram.

This procedure is shown in Figure 8, which relates to the same winding as Figure 7 yet chorded by two slot pitches, that is, the upper coil side of the slot 1 is connected to the lower coil side of slot 11. The lower coil side of slot 11 is to be denoted by 1'. The digits 1 to 24 are the end points of the vector diagram of the upper layer. The digits 1' to 24' are the end points of the vector diagram of the lower layer. These latter digits run in opposite direction to those of the upper layer, for the coil sides 1', 2' . . . carry currents of a direction opposite to that of the coil sides 1, 2, 3 . . . . Since the coil sides 1 and 15', 2 and 16', 3 and 18' . . . lie in the same slots, 1 is to be connected with 15', 2 with 16' . . . . The centers of these connecting vectors (the full circles) are the end points of the resultant diagram of the magnetomotive forces. Equations 7 and 8 can be derived from the diagram, Figure 8, in the same way as equation 59 was derived from the diagram, Figure 7.

A comparison between the vector diagrams, Figure 7 and Figure 8, makes evident the influence of chording. The shape of the resultant vector diagram, Figure 8, is much closer to a circle, that is, to the ideal case where harmonics do not exist at all, than that of Figure 7; the differences between the vectors which represent the magnetomotive forces over the different slots are much smaller than in Figure 7.

Appendix II. Harmonic Leakage Reactance of a Squirrel-Cage Winding With Respect to the Synchronous Wave

In applying equation 56 from Appendix I to the squirrel-cage winding make $m=Q_2$. The magnetomotive force of one slot is equal to $\sqrt{2}I_{2,1}$. This magnetomotive force is taken as a unit. Thus, for the squirrel-cage winding

$$X_g = 4.06\times 2\pi^2 f_1\,\frac{p\tau l_i}{gK_c}\,\frac{10^{-8}}{Q_2^2}\sum_{Q_2}\left(\frac{f_t}{\sqrt{2}I_{2,1}}\right)^2 \qquad (62)$$

Table VIII

Standstill, Rotor not Skewed

n	s_n	X_{mn} ($\times 10^{-4}$)	r_n ($\times 10^{-4}$)	$X_{(s+e)n}$ ($\times 10^{-4}$)	$r_{(s+e)n}$	r_{hn}	r_{2n}	tan θ	C_n	D_n	D_n for $r_{n=0}$	D_n for $r_{n=0}$ and $X_{(s+e)n=0}$
− 5	1	8.38	0.776	0.737	0.088	0.092	0.180	0.0788	0.847	0.167	0.153	0.085
+ 7	1	4.27	0.767	0.719	0.168	0.178	0.346	0.133	0.740	0.284	0.257	0.151
−11	1	1.73	0.762	0.708	0.409	0.535	0.944	0.227	0.501	0.516	0.485	0.348
−23	1	0.396	0.765	0.715	1.806	9.32	11.13	0.159	0.082	0.927	0.918	0.903
+25	1	0.336	0.770	0.725	2.16	18.8	20.96	0.109	0.046	0.954	0.954	0.945

Table IX

Normal Slip, Rotor not Skewed

n	s_n	X_{mn} ($\times 10^{-4}$)	r_n ($\times 10^{-4}$)	$X_{(s+e)n}$ ($\times 10^{-4}$)	$r_{(s+e)n}$	r_{hn}	r_{2n}	tan θ	C_n	D_n	D_n for $r_{n=0}$	D_n for $r_{n=0}$ and $X_{(s+e)n=0}$
− 5	+ 5.87	8.38	1.455	1.05	0.125	0.092	0.217	0.024	0.821	0.181	0.178	0.085
+ 7	− 5.83	4.27	1.446	1.03	0.242	0.178	0.420	0.041	0.708	0.295	0.295	0.151
−11	+11.72	1.73	1.67	0.932	0.54	0.535	1.075	0.04	0.483	0.518	0.518	0.348
−23	+23.4	0.396	3.19	0.518	1.31	9.32	10.63		0.086		0.914	0.903
+25	−23.4	0.336	3.19	0.528	1.57	19.8	20.37		0.047		0.953	0.945

The Goerges' vector diagram of the magnetomotive forces is here a polygon with Q_2/p sides which has almost the form of a circle. Each side has the length $\sqrt{2}I_{2,1}=1$. The angle between two radii is equal to

$$\delta=\frac{2\pi}{Q_2}p \tag{63}$$

(see Figure 9) and, therefore, the radius of the circle (per unit) is

$$R=\frac{1}{2\sin\frac{\delta}{2}} \tag{64}$$

The radii of the diagram represent the magnetomotive forces of the different slots. The amplitude is the same for all slots; the phase angles are different. The projections of all radii on an arbitrarily chosen time line give

$$\sum_{Q_2}\left(\frac{f_t}{\sqrt{2}I_{2,1}}\right)^2=\sum_{K=1}^{K=Q_2}(R\sin K\delta)^2=\frac{1}{4\sin^2\frac{\delta}{2}}\frac{Q_2}{2} \tag{65}$$

thus

$$X_g=1.015\times\pi^2 f_1\frac{p\tau l_i}{gK_c}\frac{10^{-8}}{Q_2}\frac{1}{\sin^2\frac{p\pi}{Q_2}} \tag{66}$$

X_g is the total reactance including that of the synchronous wave. By comparison of equation 66 for X_g with equation 11a,

$$\sum_{K_2=0}^{K_2=\infty}\left(\frac{1}{K_2\frac{Q_2}{p}+1}\right)^2=1+\sum_{K_2\neq 0}^{K_2=\infty}\left(\frac{1}{K_2\frac{Q_2}{p}+1}\right)^2=\frac{1}{\left(\frac{\sin\frac{p\pi}{Q_2}}{\frac{p\pi}{Q_2}}\right)^2\eta^2_{2(n-1)}}$$

or

$$\sum_{K_2\neq 0}\left(\frac{1}{K_2\frac{Q_2}{p}+1}\right)^2=\frac{1}{\xi^2_{2(n-1)}\eta^2_{2(n-1)}}-1 \tag{67}$$

where

$$\xi_{2(n-1)}=\frac{\sin\frac{p\pi}{Q_2}}{p\pi/Q_2} \tag{68}$$

By comparison of equation 66 for X_g with equation 11 from the body of the paper,

$$\sum_{K_2\neq 0}\left(\frac{1}{K_2Q_2+p}\right)^2=\frac{1}{p^2}\left(\frac{1}{\xi^2_{2(n'-p)}\eta^2_{2(n'-p)}}-1\right) \tag{69}$$

Appendix III. Harmonic Leakage Reactance of a Squirrel-Cage Winding With Respect to the Harmonics

The current induced in the rotor bars by the n'th stator harmonic will be denoted by $I_{2n'}$. The total reactance produced by this current in the rotor is, according to equation 62 from Appendix II,

$$X_{gn'}=4.06\times 2\pi^2 f_1\frac{n'\frac{p\tau}{n'}l_i}{gK_c}\frac{10^{-8}}{Q_2^2}\times\sum_{Q_2}\left(\frac{f_t}{\sqrt{2}I_{2n'}}\right)^2 \tag{70}$$

With

$$\delta_{n'}=\frac{n'2\pi}{Q_2} \tag{71}$$

and $\sqrt{2}I_{2n'}=1$, the radius of the Goerges diagram is (per unit)

$$R=\frac{1}{2\sin\delta_{n'}/2} \tag{72}$$

Thus,

$$\sum_{Q_2}\left(\frac{f_t}{\sqrt{2}I_{2n'}}\right)^2=\sum_{K=1}^{K=Q_2}(R\sin K\delta_{n'})=\frac{1}{4\sin^2\frac{\delta_{n'}}{2}}\frac{Q_2}{2} \tag{73}$$

and

$$X_{gn'}=1.015\times\pi^2 f_1\frac{p\tau l_i}{gK_c}\frac{10^{-8}}{Q_2}\frac{1}{\sin^2\frac{n'\pi}{Q_2}} \tag{74}$$

It follows from equation 17 that

$$X_{gn'}=1.015 f_1\frac{p\tau l_i}{gK_c}\eta_{2n'}{}^2 10^{-8}\times\left[\sum_{K_2\neq 0}\left(\frac{1}{K_2Q_2+n'}\right)^2+\frac{1}{n'^2}\right] \tag{75}$$

Therefore,

$$\sum_{K_2\neq 0}\left(\frac{1}{K_2Q_2+n'}\right)^2=\frac{1}{n'^2}\left(\frac{1}{\xi_{2n'}{}^2\eta_{2n'}{}^2}-1\right) \tag{76}$$

where

$$\xi_{2n'}=\frac{\sin\frac{n'\pi}{Q_2}}{\frac{n'\pi}{Q_2}} \tag{77}$$

Table X. Factor D_c

Influence of Chording on the Damping for Three-Phase Machines

$q_1=2$	$\frac{W}{\tau}=5/6$	4/6	3/6	$\frac{Q_1}{p}$			
	1.01	1.0	1.0	10			
	1.05	1.0	1.0	15			
	1.1	1.0	1.0	20			
$q_1=3$	$\frac{W}{\tau}=8/9$	7/9	6/9	5/9	$\frac{Q_1}{p}$		
	1.02	1.04	1.0	0.95	12		
	1.05	1.08	1.0	0.95	20		
	1.08	1.15	1.0	0.95	30		
$q_1=4$	$\frac{W}{\tau}=11/12$	10/12	9/12	8/12	7/12	6/12	$\frac{Q_1}{p}$
	1.08	1.15	1.1	1.0	0.95	1.0	18 to 36
$q_1=6$	$\frac{W}{\tau}=17/18$	15/18	13/18	12/18	10/18	9/18	$\frac{Q_1}{p}$
	1.1	1.4	1.15	1.0	0.95	1.0	24 to 50
$q_1=8$	$\frac{W}{\tau}=22/24$	20/24	18/24	16/24	14/24	12/24	$\frac{Q_1}{p}$
	1.2	1.6	1.3	1.0	0.95	1.0	30 to 60

Table XI

Stator Harmonic, n	Rotor Harmonics										
	$K_2=0$	$K_2=-2$	$K_2=+2$	$K_2=-4$	$K_2=+4$	$K_2=-6$	$K_2=+6$	$K_2=-8$	$K_2=+8$	$K_2=-10$	$K_2=+10$
+1	+1	−5	+7	−11	+13	−17	+19	−23	+25	−29	+31
−5	−5	−11	+1	−17	+7	−23	+13	−29	+19	−35	+25
+7	+7	+1	+13	−5	+19	−11	+25	−17	+31	−23	+37
−23	−23	−29	−17	−31	−15	−41	−5	−47	+1	−53	+7
+25	+25	+19	+31	+13	+37	+7	+43	+1	+49	−5	+55

Table XII

n	s_n	X_{mn}	$\tau_{(s+e)n}$	K_{dpn}	τ_{hn}	τ'_{hn}	D_n
− 5	1	12.9×10^{-3}	4.46	0.200	12.5	16.96	0.945
+ 7	1	3.6	16	0.149	47.7	63.7	0.986
−11	1	0.81	71	0.110	216	287	0.995
−23	1	0.34	169	0.149	514	683	1.0

If I_{2n} is introduced instead of I_{2n}',

$$\delta_n = \frac{n2p\pi}{Q_2} \tag{78}$$

$$R = \frac{1}{2\sin\delta_n/2} \tag{79}$$

$$X_{gn} = 1.015\times\pi^2 f_1 \frac{p\tau l_i}{gK_c}\frac{10^{-8}}{Q_2}\frac{1}{\sin^2\frac{np\pi}{Q_2}} \tag{80}$$

By comparing with equation 17a

$$\sum_{K_1\neq0}\frac{1}{\left(K_1\frac{Q_2}{p}+n\right)^2} = \frac{1}{n^2}\left[\frac{1}{\xi_{2n}^2\eta_{2n}^2}-1\right] \tag{81}$$

where

$$\xi_n = \frac{\sin\frac{np\pi}{Q_2}}{\frac{np\pi}{Q_2}} \tag{82}$$

Appendix IV. $\sum\left(\frac{K_{dpn}}{n}\right)^2 D_n$ for the Slot Harmonics

The order of the stator-slot harmonics is given by the equation [48]

$$n_{sl} = \mp K\frac{Q_1}{p}+1 \tag{83}$$

It can always be assumed, for the slot harmonics of a higher degree than one, that D_n is equal to one (see examples in the body of the paper). Thus,

$$\left[\sum\left(\frac{K_{dpn}}{n}\right)^2 D_n\right]_{\substack{n=n_{sl}\\K>1}} \approx K_{dp1}^2\left[\frac{1}{(-4mq+1)^2}+\frac{1}{(4mq+1)^2}+ 2\frac{1}{(2mq)^2}\sum_{K>1}\frac{1}{K^2}\right] \tag{84}$$

When one is neglected with respect to $3Q/p$, this gives

$$\left[\sum\left(\frac{K_{dpn}}{n}\right)^2 D_n\right]_{\substack{n=n_{sl}\\K>1}} \approx K_{dp1}^2\left[\frac{1}{(-4mq+1)^2}+\frac{1}{(4mq+1)^2}+ 0.786\frac{1}{(2mq)^2}\right] \tag{85}$$

Nomenclature

A = ampere-conductors per one inch of the circumference
c = chording in slot pitches
D = damping factor
D_t = total damping factor
f_1 = line frequency
f_2 = frequency of the rotor currents
f = magnetomotive force
g = gap
K_d = distribution factor
K_p = pitch factor
K_c = carter factor
K_s = saturation factor
K_1, K_2 = constants
l_i = ideal core length
m' = order of the rotor harmonic
m = number of phases
n', n = order of the stator harmonic
n_{sl} = order of the slot harmonic
n_c = number of effective conductors per slot
N or N_{ph} = number of turns per phase
N = numerator of the fraction $q=\frac{N}{\beta}$
p = number of pole pairs
q = number of slots per pole per phase
Q = total number of slots
R = radius
s = slip of the rotor with respect to the synchronous wave
s_n' = slip of the rotor with respect to the n'th harmonic
S = skew (in inches)
W = coil width (in inches)
X_m = reactance due to the main flux
X_d = reactance due to the differential leakage
X_g = reactance due to the total flux in the gap
β = denominator of the fraction $q=\frac{N}{\beta}$
δ = angle between two radii of the Gorges vector diagram for the squirrel-cage rotor
η = skew factor
ξ = coupling factor of the squirrel-cage rotor
τ = pole pitch
τ = leakage coefficient
τ_{2s} = slot leakage coefficient of the rotor
τ_{2h} = differential leakage coefficient of the rotor
τ_{2e} = end-winding leakage coefficient of the rotor
τ_1 = total leakage coefficient of the stator winding

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31. Transformer Ratio and Differential

An Improved Electronic Control for Capacitor-Discharge Resistance Welding

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THE capacitor-discharge system of resistance welding has been used widely in the past four years because:

1. The current required to produce welds in material of a given thickness can be obtained with lower kilovolt-ampere demand on the power supply than is required by a corresponding single-phase a-c resistance welder.
2. It provides a three-phase balanced load on the power-supply system as compared with the single-phase load of a-c resistance welders.
3. It provides precise control of the magnitude of the welding current on successive welds in spite of varying line voltages. This is vitally necessary in the welding of aluminum.

The designers of the original electronic controls for this system were faced with the usual problem of taking existing parts and circuits and applying practical engineering ingenuity to build a complete control with little background experience for a guide in the special problems involved. Time has now permitted greater study of the component parts of the control and further consideration has been given to the important details that could be considered only briefly earlier. Field experience has aided greatly in obtaining a better perspective of the many problems. An important factor in the development of the system has been the improvement in the techniques of welding aluminum and its alloys.

The improvement in welding techniques and in the design of structures to be welded has resulted in a growing need for a capacitor-discharge welding control which will give higher speeds and will require less maintenance. The improved control described in this paper is the result of intensive experimental work to meet these two requirements.

The basic elements of a capacitor-discharge system are:

1. A capacitor bank in which energy for the resistance weld is stored.
2. A rectifier circuit for converting a-c to d-c power to charge the capacitor bank and a charge-control circuit for regulating the quantity of electric energy stored in the capacitors.
3. A discharge system to convert the stored electrostatic energy into welding current.
4. A flux-reset circuit to restore the welding transformer flux to the same value between welds.
5. A sequence-control system including a forge timer to co-ordinate the operation of elements 2 and 3 with the mechanical system of the welding machine.

A simplified functional diagram of the complete control is shown in Figure 2. A time sequence of events for the system in Figure 2 is shown in Figure 3. Rather than describe the complete control as a single circuit, the various functions are individually described in the following sections. Because the sequencing circuits in item 5 resemble closely those used with single-phase a-c welders, they are not fully described here.

The outstanding difference between this system and previous ones lies in the discharge circuit. Previous capacitor-discharge systems have used a unidirectional discharge of welding current. This new system uses what will be called a full-cycle discharge.

The Discharge Circuit

The improved full-cycle discharge circuit which is shown in Figure 4 contains:

1. The capacitor bank.
2. The welding transformer and its secondary-load circuit.
3. Two ignitron tubes and their associated thyratron firing tubes.
4. A flux-resetting rectifier.

The welder discharge circuit can be represented approximately by an equivalent inductance, resistance, and capacitance connected in series with a switch. This equivalent circuit is oscillatory for the values of resistance, inductance, and capacitance usually required with existing welders used in resistance welding a wide range of thicknesses of aluminum.

In the full-cycle system, tubes 1 and 2 are connected in inverse parallel and, with proper control of the firing tubes, will permit a single positive and a single negative half cycle of current flow during discharge.

The advantages obtained with the full-cycle discharge circuit are more easily understood if the disadvantages of the unidirectional-discharge system are known. These are summarized here and described fully in the appendix.

During the original development of the capacitor-discharge system, it was believed that better welds could be obtained in aluminum with a nonoscillatory welding current. Therefore, all earlier models of this control contained a shunt ignitron tube to prevent any oscillation of the discharge current.

The disadvantages of this type of circuit are:

1. Considerable magnetic energy remains in the transformer to be dissipated at the electrodes upon separation after the weld. This energy is sufficient in most cases to create a spark as the electrodes separate. Electrode sparking destroys the protective coating of pure aluminum used on certain alloys of aluminum and provides a starting point for atmospheric corrosion. Therefore, most specifications for capacitor-discharge welders require elimination of sparking. This requires the use of a contactor in most cases to short-circuit the welding-transformer primary or secondary winding between welds. These contactors have been both ineffective and troublesome.
2. Reversing contactors are required in the discharge circuit to reverse the flux and provide equal currents on successive welds. These two contactors and auxiliary relays as well as the primary-short-circuiting contactor in 1 require additional main-

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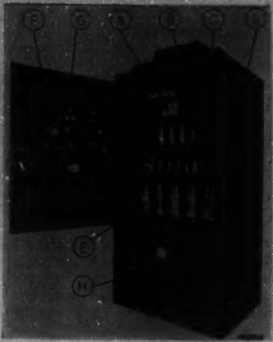


Figure 1. A capacitor-discharge welding control with the front door open

(*A*) Sequence panel, (*B*) Flux-resetting rectifier, (*C*) Charge-control and blocking circuits, (*D*) Rectifier tubes, (*E*) Firing tubes for discharge ignitrons, (*F*) Capacitor-voltage-adjusting potentiometer, (*G*) Sequence-time potentiometers, (*H*) Capacitor tap switches

tenance and limit the maximum operating speed of the welder and control.

It is possible to design a capacitor-discharge control which combines a unidirectional discharge circuit with a flux-resetting rectifier to eliminate reversing contactors. However, this circuit combination contains the same problem with regard to electrode sparking as described in paragraph 1.

In the operation of the full-cycle discharge circuit the secondary of the welding transformer has been closed and the flux-resetting rectifier has been turned off by 4*CR* contacts when point 4 in the timed sequence of Figure 3 has been reached. A single impulse of positive voltage is applied across leads 90–91 to the grid of firing tubes 7 in Figure 4 by the weld-delay timer to initiate the discharge. Tube 1 in Figure 4 is then fired and discharge current will flow as shown by the oscillograms in Figures 5A and 5B.

Tube 1 conducts current from time T_0 to T_2 in Figure 5A, and tube 2 fires at T_2 and conducts current from T_2 to the end of the discharge at T_4. The discharge current and voltage are displaced by a phase angle depending upon the circuit parameters. At the end of the discharge, the capacitor-bank voltage has the same polarity as the initial-charge voltage. This means that a considerable portion of the original stored energy is returned to the capacitor bank for use on the next weld. It also means that the speed of operation of the control is increased since the charging time of the capacitor bank on repeat operation is decreased.

The curve of flux versus magnetizing ampere-turns for a typical welding transformer is shown in Figure 6. Flux conditions in Figure 6 correspond to the current and voltage oscillograms of Figure 5A. Before the weld, assume the flux has been fixed at point *K* (the flux-reset point). During discharge the flux changes along the curve through points *M*, *N*, and *G* to reach a maximum value *B* (Figure 6) at time T_1 in Figure 5A. From T_1 to T_2 the flux decreases along the curve through points *C* and *E* to point *F*. From time T_3 to T_4 the flux is again increased and the final value is reached at point *G* at time T_4. The area *GHJ* is proportional to the residual energy capable of being dissipated in the transformer core at the end of the weld. The short-circuiting effect of the secondary circuit prevents any substantial decrease in this energy until the electrodes are opened. This energy must then be dissipated at the electrodes upon separation.

The change in the welding-transformer flux during discharge with the unidirectional circuit also is shown by Figure 6 and described in detail in the appendix. It is important to note that with identical values of circuit constants (capacitance, charge volts, transformer-turns ratio, and so forth) for the unidirectional discharge the residual magnetic energy to be dissipated at the electrodes (proportional to the area *CDE*, Figure 6) is ten times as large as with the full-cycle discharge. Substantially the same comparative values of residual energy can be obtained for other than maximum settings represented in Figure 6. The reduction in residual energy obtained therefore with full-cycle discharge in most cases is sufficient to permit separation of the electrodes at any time after the discharge without visible sparking even though a primary short-circuiting contactor is not used.

In the description of the discharge circuit, principal consideration has been given to normally oscillatory conditions. In the welding of aluminum alloys thicker than one-tenth inch or in the welding of most thicknesses of stainless steels, circuit parameters will not permit oscillation of the discharge current. Special consideration has been given to operation under these conditions. The problem of electrode sparking is not serious for this condition because these materials all introduce additional resistance in the secondary circuit to permit the core flux to return much more rapidly toward the residual value before the electrodes open. However, it is important to provide a generous design of transformer when operating under these conditions to avoid sparking from increased saturation. Emphasis should be placed on the design of the secondary circuit of the welder wherever possible to permit oscillation and make such a generous transformer design unnecessary.

Flux Resetting

The flux-resetting rectifier supplies current to parts of the primary winding of the welding transformer of opposite polarity to the first half cycle of discharge current. The resultant magnetomotive force applied to the transformer core resets the flux to the same value, *K* in Figure 6, between each weld. If the flux is not reset, the transformer iron tends to become saturated resulting in a greatly decreased welding current.

Certain important factors must be considered to obtain an economical rectifier design:

1. The rectifier must be capable of resetting the core flux in a minimum time determined from the maximum desired operating speed. Any attempt to reset flux with a closed secondary circuit in as short a time as the discharge time will require a discharge with a current of the same magnitude as the weld current. Therefore, flux resetting must be accomplished while the electrodes are open.

2. The self-inductance of that portion of the primary winding carrying flux-resetting current must be known. Current-limiting resistor *R*-47 must have a sufficient re-

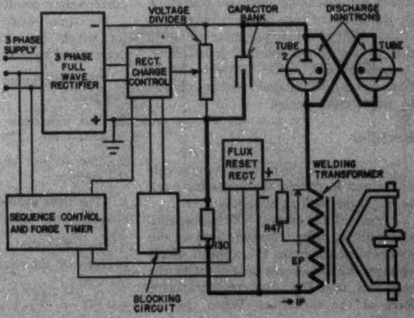


Figure 2. Simplified functional diagram of the capacitor-discharge welding system

sistance to make the time constant of the circuit small which will permit the flux-resetting current to reach its final value quickly.

3. The type of rectifier used for flux resetting must be selected with regard to maximum ripple-voltage output under load. Any ripple voltage in this circuit will appear across the primary and secondary windings of the welding transformer. If this voltage is too large, a spark will result if the secondary electrodes are accidentally short-circuited by the operator when inserting material in the machine for welding. The material damage resulting would be as unsatisfactory as electrode sparking after each weld.

4. The ampere-turns supplied by the rectifier should be sufficient to establish the flux-reset point K of Figure 6 at a value past the knee of the flux-ampere-turn curve to minimize the effect of variations in the magnetic characteristics of the iron in different transformers of the same rating.

The direct current required to reset the welding-transformer flux therefore can be less than three amperes for nearly all transformers if the above factors are properly considered.

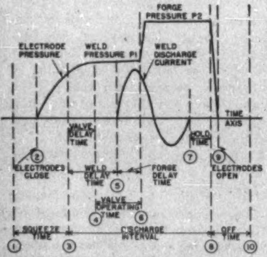


Figure 3. Sequence of operation of a capacitor-discharge welding control

It is important to note that with a flux-resetting rectifier the initial value of flux K, Figure 6, can be greater than the residual value obtained with the unidirectional-discharge circuit. Therefore, by changing the type of discharge circuit, it is possible to obtain a greater change in flux from a given welding transformer. A greater peak value of weld current therefore can be obtained without change in transformer design.

Rectifier

The purpose of the rectifier is to provide d-c power to charge the capacitor bank in which the welding energy is stored.

In general, the current which flows into a capacitor being charged from a d-c source depends upon the applied direct voltage, the impedance in series with the capacitor, and the counterelectromotive force of the capacitor. When the capacitor voltage is zero, the initial current is limited only by the applied direct voltage and the series impedance. If the applied voltage is high and the series impedance is low, the initial current will be high. Since the rectifier is the d-c source, a large initial current would result in a large kilovolt-ampere demand from the power system. Therefore, the capacitor current at the start of charge must be limited if the kilovolt-ampere demand on the power system is to be a reasonable value.

The capacitor current can be limited either by resistance in series with the capacitor or by voltage regulation in the rectifier, obtained for instance by building leakage reactance into the rectifier transformer. In the system being described, the large power losses which are incurred in a series resistance prohibit its use. Hence, current limiting is obtained by means of leakage reactance.

Under ideal conditions, the capacitor current at the start of the charge is equal to the steady-state short-circuit current of the rectifier, both in average and peak value. However, when reactance is present in the rectifier, the rectifier tubes must be fired at the proper point in the cycle or an abnormally high transient current may result.

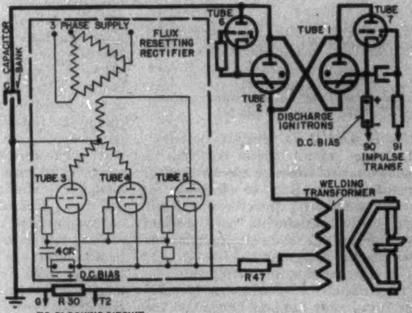


Figure 4. (right) Schematic diagram of the full-cycle discharge circuit

The steady-state short-circuit current for which the rectifier is designed is determined by the following factors:

1. The maximum capacitance to be charged.

2. The maximum voltage to which the capacitors are to be charged.

3. The no-load output direct voltage of the rectifier.

4. The time available for charging maximum capacity to maximum voltage.

The relationship between these factors is given by the equation:

$$e_c = E\left(1-\epsilon^{-\frac{t}{RC}}\right) \tag{1}$$

where

e_c = capacitor voltage
E = applied direct voltage
t = time in seconds
$R = \frac{E}{I_{sc}}$
I_{sc} = rectifier short-circuit current

By rearrangement and solution for the short-circuit current:

$$I_{sc} = \frac{EC}{t}\log_\epsilon \frac{1}{\left(1-\frac{e_c}{E}\right)} \tag{2}$$

This derivation assumes that the applied voltage is pure direct current (without ripple). As long as the rectifier ripple voltage is small, the error due to this assumption is negligible. The derivation also assumes that reactance limiting is equivalent to resistance limiting where the equivalent limiting resistance is

$$R = \frac{E}{I_{sc}}$$

as explained in the derivation. Laboratory tests show this assumption to be satisfactory for a three-phase full-wave rectifier charging a capacitor bank to one half (or less) of the no-load direct voltage of the rectifier.

A three-phase full-wave rectifier, with a no-load output direct voltage of approximately 6,000 volts, is used to charge the capacitor bank. The rectifier must charge 2,640 microfarads to 2,800 volts in five-sixths second. The calculated value of short-circuit current from equation 2 is 13 amperes which checks closely with the value measured on the rectifier.

The diagram of the rectifier is given in Figure 7. Tubes 4, 5, and 6 are grid-controlled thyratrons, while tubes 1, 2, and 3 are used as phanotrons. Load current can be controlled by this circuit since all of it must flow through the controlled tubes. The grid voltage on the controlled tubes consists of a fixed alternating and a variable direct voltage. The fixed alternating voltage is supplied

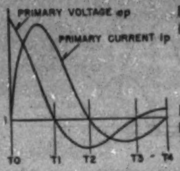
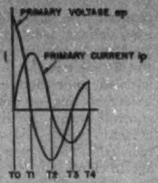


Figure 5A (left). Oscillogram of welding-transformer-primary voltage and current for the full-cycle discharge

With a maximum capacitance

Figure 5B (right). Oscillogram of welding-transformer-primary voltage and current for the full-cycle discharge

With minimum capacitance



by the secondaries of transformers whose primaries are connected to the low-voltage winding of the rectifier transformer. The phase position of this alternating grid voltage is 120 degrees lagging the voltage of the rectifier transformer phase connected to the tube's anode. The variable direct voltage is supplied by the charge-control system which will be described in a later section. By varying the magnitude of the direct voltage, the rectifier current is controlled from maximum to minimum.

Figure 8A shows the positive no-load voltage appearing between anode and cathode of tubes 4, 5, and 6. Because of the large leakage reactance in the rectifier transformer, the voltage appearing across each tube when the rectifier is loaded is far different from those shown. However, the no-load voltages are helpful in determining where a tube can begin to conduct and where it will stop conducting.

If the grid voltage is made more positive than critical value near point *A*, the rectifier will conduct minimum current as shown in Figure 8B. The current continues to flow after the no-load voltage is negative because of the leakage reactance in the transformer. The rectifier produces maximum steady-state short-circuit current when the voltages on the grids of the controlled tubes become more positive than the critical values at point *B* as shown in Figure 8C. Although this firing point produces maximum steady-state short-circuit current, the firing point must be advanced as the counterelectromotive force of the capacitor increases in order to maintain maximum charging current. This is accomplished by making the variable direct grid voltage more positive than shown in Figure 8C in about 30 milliseconds after the start of charge. Each tube in the rectifier will then fire as soon as its anode voltage exceeds its cathode voltage by the amount of its breakdown voltage and the firing point of the rectifier tubes will advance automatically as the capacitor bank is charged. If the rectifier tubes are fired before point *B* for the first several cycles after the capacitor is started to charge, a transient will occur in the rectifier current which may be as much as double the peak value of the steady-state short-circuit current. This transient is undesirable because it may exceed the peak current rating of rectifier tubes and may even cause an arc-back or loss of control in the rectifier. Transients can be avoided if the rectifier tubes are fired later than point *B* at the start of charge and then gradually phased forward as will be described in the discussion of the charge-control circuit.

Charge-Control Circuit

The functions of the charge control are threefold. It regulates the voltage to which the capacitor bank charges, maintains the capacitor bank at that voltage until a welding discharge is desired, and blocks the rectifier during the discharge period.

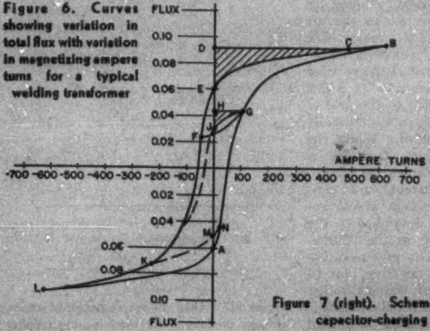
The charge-control circuit is shown schematically in Figure 9. It is a special two-stage direct-coupled amplifier consisting of the amplifier tubes, their power supplies, and a voltage supply which serves as a reference in regulating the capacitor-bank voltage.

The input to the charge control is a part of the voltage developed across resistor 27. This voltage is proportional to that across the capacitor bank. The output of the charge-control circuit is the variable direct voltage which is applied to the grids of the controlled rectifier tubes as described in the rectifier section. This voltage varies from a positive value, equal to the peak of the alternating grid voltage, to a negative value of approximately three times as much, as shown by the curve in Figure 10.

The curve in Figure 10 is helpful in understanding the operation of the charge-control circuit. Assuming that the charge on the capacitor bank is zero, the grid of tube 10 is positive by the amount of the regulated reference voltage appearing across potentiometer 6. The reference voltage selected by the potentiometer determines the voltage to which the capacitor bank charges. Since the grid of tube 10 is positive, corresponding to point *A*, the tube will conduct maximum anode current and will produce sufficient voltage across its plate-load resistor to reduce the current through tube 9 practically to cutoff. Thus the voltage appearing across resistor 4 is negligible. The output voltage of the charge control is the algebraic sum of the voltage across the resistor 4 and the voltage between points 51 and 1 on resistor 7. Therefore, lead 43 is positive with respect to lead 1. This positive voltage on the grids of the controlled rectifier tubes causes the rectifier to pass maximum current, thus charging the capacitor bank.

As the capacitor bank becomes charged the voltage on the grid of tube 10 decreases. However, the output voltage of the charge control remains unchanged until the grid voltage of tube 10 reaches point *B*. Then a small decrease in the grid voltage of tube 10 will cause the output voltage to swing negative to a value in region *C*. This decreases the rectifier current to a small value which is

Figure 6. Curves showing variation in total flux with variation in magnetizing ampere turns for a typical welding transformer



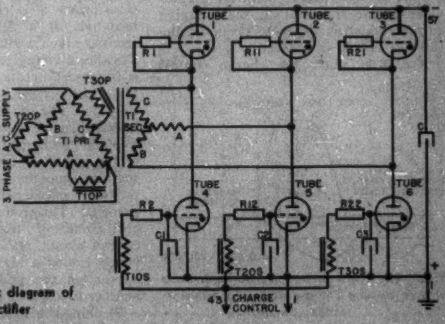


Figure 7 (right). Schematic diagram of capacitor-charging rectifier

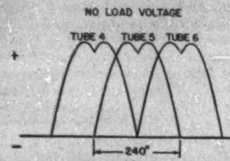


Figure 8A. Positive no-load voltage appearing across rectifier tubes 4, 5, and 6 in Figure 7

just enough to supply the bleeder current and the leakage of the capacitor bank. If this current is insufficient to maintain the capacitor voltage at the desired value, the charge control causes the rectifier to increase its output, thus raising the capacitor-bank voltage. In this manner, the capacitor voltage is regulated within two per cent of the desired value until the capacitors are discharged to produce a weld. The capacitor-bank voltage can be adjusted to any value between 1,500 and 2,800 volts by means of potentiometer 6.

When the weld is made, the blocking circuit, which will be described later, decreases the grid voltage of tube 10 to point *D*. The grids of the rectifier tubes are now highly negative, thus preventing the rectifier from conducting current.

The grid voltage of tube 10 remains at point *D* until the discharge is completed. At this time the blocking voltage becomes zero and grid voltage returns to point *A*. However, the output voltage of the charge control does not become positive immediately because of capacitor 9 shunting resistor 4 as shown in Figure 9. The discharge-time constant of this resistor–capacitor combination is so selected that the output voltage of the charge control does not reach its full positive value for two or three cycles after the grid voltage of tube 10 returns to point *A*. When the output of the charge control reaches point *G* the rectifier begins to conduct. As this voltage becomes more positive, the rectifier current gradually increases until it reaches maximum value when the charge-control output voltage reaches point *B*. In this manner, the transient in the charging current which is likely to occur at the start of charge is avoided.

Not only must the charge control regulate the capacitor-bank voltage at any desired value between 1,500 and 2,800 volts, but it must bring the voltage to this value in the shortest possible time without undershooting or overshooting. Undershooting occurs when the charge control begins to reduce the rectifier current before the desired voltage is reached. This causes the capacitor voltage to climb slowly to the correct value. Overshooting occurs when the charge control does not reduce the rectifier current soon enough so that the last half cycle of current conducted by the rectifier will cause the capacitor voltage to become greater than the desired value.

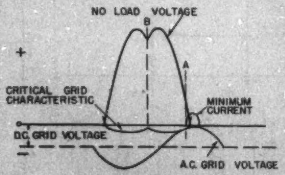


Figure 8B. Grid voltage and no-load anode voltage on tube 5 in the rectifier for minimum rectifier current

By properly selecting the load resistance of tubes 9 and 10, capacitor 9, and the magnitude of the reference voltage, both overshooting and undershooting can be limited to less than two per cent of the voltage to which the capacitor is being charged even at 200 operations per minute. Thus the charge control is capable of accurately controlling the capacitor-bank voltage and thereby the energy which is available for performing the weld.

Blocking Circuit

The blocking circuit prevents the rectifier from passing current while the discharge current is flowing. If rectifier current is allowed to flow during this time, tube 1 in Figure 4 continues to conduct the rectifier current through the primary of the welding transformer after the discharge is over. This prevents the capacitor bank from being recharged.

The schematic diagram of the blocking circuit is given in Figure 11. The blocking indication is the voltage appearing across resistor 30 when the capacitor discharge current is flowing. This voltage is applied to the low-voltage winding of transformer 8 which is designed for operation at a frequency of one cycle per second so that it can transform the low-frequency voltages which appear across resistor 30 when certain combinations of capacitance and inductance are used in the discharge circuit. The high-voltage winding of transformer 8 is connected to the a-c terminals of a full-wave copper-oxide rectifier which rectifies the blocking signal. The direct-voltage output of the rectifier is impressed across neon tube 11 which limits the voltage applied to the grid of tube 12. The grid of this tube is normally biased just beyond cutoff. When the welding discharge occurs, the grid is driven positive, which causes the tube to conduct maximum current and thus produces maximum voltage across resistor 9. This voltage is applied to the grid circuit of tube 10 in the charge-control circuit making its grid negative which causes a highly negative bias to appear on the grids of the controlled rectifier tubes, thus blocking the rectifier.

Blocking continues until the discharge current falls to about two amperes. In a full-cycle discharge the reverse ignitron goes out a fraction of a cycle later. Since some delay exists in the charge-control circuit because of capacitor 9 the ignitron goes out before the rectifier can come on again. Hence the blocking circuit accomplishes its purpose and allows the rectifier to begin charging the capacitor bank at the earliest possible moment after the discharge is over.

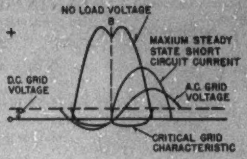


Figure 8C. Grid voltage and no-load anode voltage on tube 5 for maximum short-circuit rectifier current

In welding thick aluminum, the discharge may become nonoscillatory. In some cases the discharge may last as long as one-half second and the current may fall as low as one ampere before the series ignitron goes out. The electronic blocking circuit described does not have sufficient sensitivity to block when the discharge current falls below two amperes. Since nonoscillatory discharges occur only in welding thick aluminum, it was felt that the use of another stage of amplification was not justified. Therefore a relay contact is used in the anode circuit of tube 10 of the charge control circuit in Figure 11 to accomplish the blocking on nonoscillatory discharges. This contact opens just before the welding discharge begins and closes when the discharge interval is over. As long as the contact is open, tube 9 conducts maximum current and maximum voltage appears across resistor 4. This blocks the rectifier as described in the charge-control section. Electronic or contactor blocking is selected by a toggle switch.

In previous designs, blocking was accomplished solely by the relay-contact method. The electronic-blocking circuit automatically allows the rectifier to start charging the capacitor bank the instant

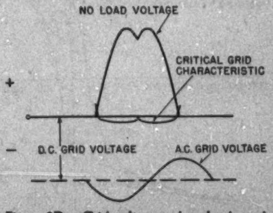


Figure 8D. Grid voltage and no-load anode voltage on tube 5 for blocking the rectifier

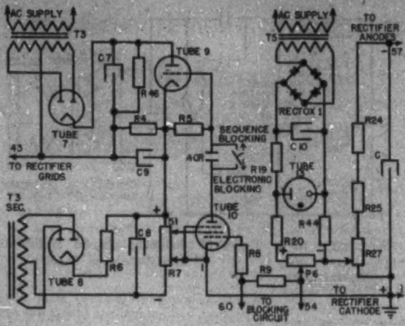
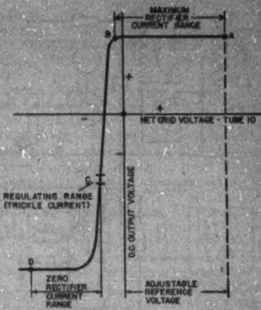


Figure 9 (left). Charge-control-circuit schematic diagram

Figure 10 (right). Curve showing regulating action of charge-control circuit in Figure 9

The output direct voltage of the charge-control circuit is plotted against the net grid voltage of tube 10



the weld is over. Therefore the electronic circuit has an advantage over the relay-contact method, where it can be used, because it allows the welding machine to be operated at higher speeds.

Forge-Timing Circuit

Forging, as applied to resistance welding, is the application of an additional pressure to the material being welded at some time during the welding period. Although forging has been applied only recently to capacitor-discharge welders, most welding engineers agree that it is instrumental in producing crack-free welds in aluminum.

Forging is obtained by several different methods, depending on the design of the welding machine. One method is the energizing of a solenoid air valve which acts to increase the pressure on the welding electrodes. In any case, the problem is to obtain a rapid increase in pressure on the material being welded at some time after the welding current has reached its peak value. The time of application of pressure should be adjustable from 8 milliseconds to 192 milliseconds after the welding current is initiated. It is the function of the forge-timing circuit in the control to provide this adjustable timing period.

Welding-machine designers specify several different types of valves for forging, the characteristics of which vary widely. The minimum operating time of many of the valves used is more than the minimum forge delay required. In order to obtain the correct minimum forge delay with the slower valves, the valve must be energized before the welding current is initiated. This is accomplished by delaying the initiation of the welding current by means of a weld delay circuit as shown in Figure 3. The forge delay time is the difference between the sum of the valve delay time plus the valve operating time and the weld delay time.

Because of the many types of valves used for forging, several different methods can be used for energizing them. Some valves can be energized from d-c sources while others must be energized by the discharge from a capacitor. The system described uses a d-c power supply for providing the energy to operate the valve. The power supply and the valve and weld delay circuits are shown in Figure 12. The valve is energized by firing tube 5. Minimum valve operating time attainable is 16 milliseconds.

The operation of the circuit in Figure 12 is as follows: When a weld is to be made, relay *3CR* is energized. When the pressure on the electrodes reaches the proper value, relay *4CR* is energized. The opening of a normally closed contact on relay *4CR* creates an impulse on the grid of tube 4 which causes the tube to fire. This charges capacitors 8 and 9 through potentiometers 1 and 2 respectively. When the voltage on capacitor 8 is great enough to overcome the negative bias developed across the part of resistor 2 in the grid-cathode circuit of tube 5, the tube will fire and energize the forging valve. The valve delay time can be adjusted from 8 milliseconds to 192 milliseconds by means of potentiometer 1.

In operation, the weld delay circuit is similar to the valve delay circuit. When the voltage across capacitor 9 overcomes the negative bias on tube 6, the tube fires and discharges capacitor 6 through the low-voltage winding of transformer 12. A voltage impulse then appears across the high-voltage winding of the transformer which is connected in the grid circuit of tube 7 in Figure 4. The impulse causes the tube to fire which in turn fires tube 1 through which the capacitor bank discharges into the welding transformer.

The weld delay time is adjustable from 16 milliseconds to 48 milliseconds by means of potentiometer 2. Usually the weld delay is set for 16 milliseconds and the forge delay is varied by adjusting only potentiometer 1 in the valve delay circuit. In this manner, the forge delay can be varied from 8 milliseconds to 192 milliseconds.

Summary

Field and laboratory tests on the control described prove that the full-cycle discharge system produces welds which are metallurgically sound. Also this new discharge system increases the operating speed, aids in resetting the flux in the welding transformer, and reduces sparking at the electrodes to a point where the spark (if any) does not mark the material being welded.

Speeds up to 200 spots per minute were obtained on a large press-type welding

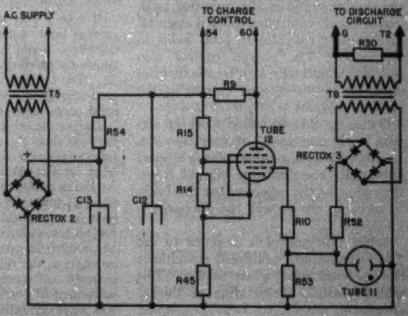


Figure 11. Schematic diagram of the blocking circuit

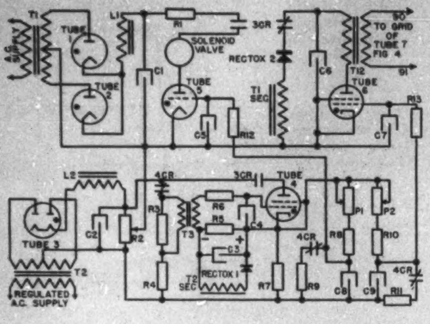


Figure 12. (left) Forge-timer schematic diagram

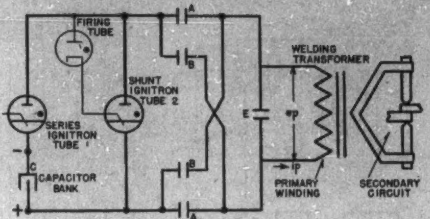


Figure 13. Schematic diagram of the unidirectional-discharge circuit

machine when using a 480-microfarad capacitor bank which was being charged to 2,500 volts. The following speed ratings have been applied to the control:

Continuous Speed, Spots Per Min	Maximum Speed, Spots Per Min	Capacitance, μf	Capacitor Volts
	200	480	2,500
200		480	2,500
	60	2,640	2,800
25		2,640	2,800

The flux-resetting circuit eliminates the large reversing contactors which were formerly used to reverse the connections to the primary of the welding transformer between each weld. This helps to make possible the high operating speeds obtainable and reduces the maintenance cost on the control. The amount of iron used in the welding transformer can be reduced since greater flux changes can be obtained because of the flux bias produced by the flux-resetting current.

The charge-control circuit controls the voltage to which the capacitor bank charges to any desired valve between 1,500 and 2,800 volts and regulates the voltage to within plus or minus two per cent of the desired value without undershooting or overshooting. Hence the energy stored in the capacitor bank can be closely controlled which is essential in obtaining consistently good welds in aluminum. The charge control also prevents abnormally high transients from occurring in the rectifier current at the start of charge.

By using the electronic blocking circuit, maximum speed of operation can be obtained because the rectifier is blocked only while the discharge current flows, thus allowing the capacitor bank to begin to recharge the instant the discharge is finished.

While this control is designed to use 2,640 microfarads, additional capacitance can be connected when needed. This makes possible the welding of thicker aluminum.

Two trends are apparent in aluminum welding. One is the welding of thin material (0.016 inch) at high speeds. The other is the welding of heavy material (0.125 inch to 0.25 inch). The control described is well suited for both types of service and should, therefore, play an important role in the welding of aluminum alloys on a production basis.

Appendix. The Unidirectional Discharge Circuit

The discharge circuit to provide unidirectional current is shown in Figure 13. In the following description of the operation during discharge it will be assumed that the contacts *A* are closed and contacts *B* and *E* are open. A discharge can be initiated by firing ignitron tube 1.

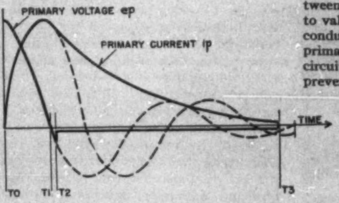


Figure 14. Oscillogram of welding-transformer-primary voltage and current for the unidirectional discharge

If tube 1 were replaced by a switch the current and voltage wave form during discharge would usually be oscillatory as shown by the dotted wave forms in Figure 14. The current and voltage for an ignitron-controlled discharge is shown by the solid curves between T_0 and T_3.

During the initial period of the discharge between time T_0 and T_1 the welding-transformer primary voltage e_p will decrease from its initial maximum value to zero. At T_1 the stored electric energy in capacitor bank *C*, less that quantity converted into heat in the weld and circuit resistance, has been transferred to the inductive parts of the system. At T_1 the polarity of e_p is reversed as the direction of energy flow is reversed. However, in the original development of the unidirectional system, it was generally believed necessary to obtain a nonoscillatory weld-current wave form. Therefore, a shunt ignitron tube 2 was connected in the system to conduct current when the reverse voltage e_p equals the critical ignitor-firing voltage for tube 2. At time T_2 on Figure 4 tube 2 conducts current and the transformer primary voltage is decreased to that of the arc drop of the shunt tube. The current in series tube 1 is then instantaneously decreased to zero. The transformer primary current i_p decreases to zero at T_3 along the exponential curve of Figure 14.

Laboratory tests have shown that in this system secondary current is usually greater than zero at time T_3 because the closed secondary circuit and shunt tube prevents the magnetic-core energy from returning to zero at the end of the discharge.

This action can be better understood from Figure 6 which shows the flux–ampere-turn curve for a typical transformer. At the start of discharge, flux ϕ will be at residual value *A*. Between time T_0 and T_1, Figure 14, it will increase to maximum value ϕ_1 at point *B* along curve *AGB*. Between time T_1 and T_2 the flux will decrease to value *C*. At T_2 the shunt tube becomes conducting and immediately both the primary and secondary circuits act as short-circuiting loops on the transformer core to prevent any appreciable change in the value of ϕ. The shunt ignitron dissipates the energy stored in the reactive circuit of the secondary loop but stops conducting current when the potential across the primary winding of the transformer has decreased to less than the arc-drop voltage of the tube. The secondary current at this point is proportional to the ampere turns required to sustain flux at point *C*. Experimental tests have shown that as long as the electrodes remain closed the time required for the secondary current to decrease to zero may be as much as two minutes. In normal operation the electrodes must be separated within one-sixth second after the end of the discharge to obtain reasonable operating speeds. When the electrodes separate secondary current must instantaneously decrease to zero and the flux changes from the value at point *C* to the residual value at point E as the sustaining magnetomotive force is removed.

A short-circuiting contactor E was used with the system in Figure 13 to short-circuit automatically the primary winding between welds. This provides a short-circuited path in parallel with the secondary circuit to eliminate the necessity for dissipating this energy at the electrodes.

To permit equal change in flux, prevent saturation, and provide equal weld currents with the circuit in Figure 13, it is necessary to provide reversing contactors A and B to operate alternately on successive discharges. The next weld therefore will cause a flux change in the transformer core from residual point E through points F, K, and L and return to A on the curve of Figure 6.

Field experience with this circuit design has shown that satisfactory operation could be obtained at moderate operating speeds up to 100 spots per minute.

From the designer's viewpoint higher speeds can be obtained only with a minimum number of mechanical devices operating in the control. Practical designs of relays and contactors have definite operating times from 8 to 90 milliseconds which when introduced in any quantity into an operating system lower its maximum operating speed. Contactors and relays also have a mechanical life which becomes shorter as operating speeds increase.

The Measurement of Lightning Currents in Direct Strokes

G. D. McCANN
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Synopsis: The magnitude and wave shape of the discharge currents in 46 direct strokes have been measured during a five-year period at 25 direct-stroke stations on structures ranging in height from 75 to 585 feet. Measurements have been made with instruments capable of recording the wave front and tail of the high-current peaks and the low-magnitude continuing currents to as low as 0.1 ampere. These data are correlated with 138 special camera photographs of strokes to open ground obtained in this investigation, with similar photographs taken in South Africa and with stroke current measurements made on the Empire State Building in New York City.

Most strokes to structures, less than 600 feet high, appear to have discharge characteristics essentially the same as those to open ground. Their probability of being struck is about proportional to height, being in the order of two strokes per year for 500-foot structures and 0.3 per year for 100-foot structures. Whereas practically all strokes to the 1,250-foot Empire State Building are initiated by an upward streamer propagating from the building to the cloud, only four such strokes have been recorded in this investigation: three out of 13 strokes to a 585-foot structure and one out of 13 to a 360-foot structure. Strokes of this type, as recorded here and to the Empire State Building, had discharge currents consisting principally of a continuous flow of low-magnitude current upon which the high-magnitude short-duration peaks are superimposed. Although this has been thought by some to be an essential characteristic of all multiple strokes, currents measured with the highly sensitive photographic surge recorder show that continuous current does not usually flow between individual component discharges in strokes to lower objects. Continuous stroke-channel luminosity for the entire duration has been recorded in one out of 61 photographs of multiple strokes to open ground.

One cathode-ray oscillogram of a high-current component shows quite high-magnitude and high-frequency fluctuations on the tail of the wave. Similar fluctuations in stroke-channel luminosity have been photographed in South Africa. These indicate that the condition is caused by charge stored on branches from the main stroke channel.

About 50 per cent of all strokes are found to be multiple, consisting of at least two component discharges. Time intervals between the beginning of successive components as high as 0.5 second have been recorded with about 80 per cent lying between 0.01 and 0.1 second. Ninety-six per cent of all recorded strokes have started with a flow of negative charge from the cloud. Eighty-six per cent were entirely negative polarity and only four per cent were entirely positive. The principal range of total stroke charge is below 100 coulombs with 55 per cent of the recorded strokes involving ten coulombs or less. Total stroke durations have been found to range up to 1.3 seconds with 90 per cent lying between 0.005 and 0.5 second. Longer-duration strokes involving a greater amount of charge than recorded in this investigation are shown for strokes to the Empire State Building.

The main wave-shape characteristics of the high-current peaks lie within fairly narrow limits. The principal range of times to crest is 0.5 to 10 microseconds, while for the times to half value it is 20 to 100 microseconds. Longer fronts and lower rates of current rise than for strokes to the Empire State Building are shown. Sixty per cent of the recorded fronts exceeded two microseconds, with 50 per cent lying between two and six microseconds. The data show a general trend of increase in wave front with increase in crest current. The maximum recorded average rate of rise is 45,000 amperes per microsecond, with 15 per cent exceeding 20,000 amperes per microsecond. The character of the continuing low-magnitude currents varies widely. The principal range of their duration is between 600 microseconds and 0.1 second, with about 50 per cent exceeding 0.003 second. The upper range of current magnitude for these continuing currents is about 1,000 amperes, but 50 amperes is seldom exceeded for more than about ten per cent of the total duration of a given component.

Lightning Recording Stations

THERE ARE two principal considerations in the choice of recording stations for the direct measurement of the currents in lightning strokes to earth. The use of very tall structures that are struck frequently has the advantage that more elaborate recording methods can be used, since only one or a few stations are required to obtain a reasonable number of records. This advantage was particularly important when only the cathode-ray oscillograph was available for recording wave shapes of lightning currents, as its cost and complexity of operation limit the number that can be used. However, the character of the strokes to such objects may not be typical of those to open ground or to such structures as transmission lines. In the use of lower structures of more typical heights, many more stations are required because each will be struck much less frequently. The number and their dispersion over a wide area limits the permissible installation and maintenance cost of each station, but permits a study of the variation of stroke characteristics with location.

The first seven direct-stroke recording stations of the Westinghouse investigation were established in 1939 and 1940 on structures ranging in height from 80 to 585 feet.[1] After they had been in service two years,[1] evidence was obtained that certain strokes to structures higher than 300 feet above the general terrain could have important characteristics differing from the strokes to lower structures or to open ground. Eighteen additional stations were then put in service on forest-fire observation towers ranging in height from 75 to 125 feet to obtain a better balance between the number of records

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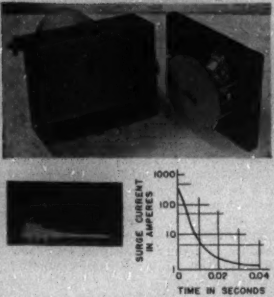


Figure 1. Photographic surge-current recorder and record of laboratory surge

obtained on low and taller structures. A list of the recording stations is given in Table I. This investigation has been made possible by the companies, institutions, and public agencies listed in Table I, who have kindly made their facilities available.

RECORDING INSTRUMENTS

The use of such a large number of stations was made possible by the development of special recording instruments, most of which have been described in detail.[2] These consist of an automatic cathode-ray oscillograph, the fulchronograph, the surge-front recorder for measuring the rate of rise of current at the start of each high-current component (which is too rapid for the fulchronograph), and the surge integrator for measuring stroke charge. An additional instrument, the photographic surge-current recorder, has been developed to record the multiple character of strokes and the continuing currents of very low magnitudes (below the 50-ampere sensitivity limit of the fulchronograph). This is shown in Figure 1, together with a record of a laboratory surge. It employs the principle of photographing the luminosity produced by flow of the surge current across a short spark gap. The film image is produced through a special aperture and set of barriers so constructed that the light from the gap spreads in a nonuniform manner over the film perpendicular to its direction of motion. The barriers confine the image to a narrow wedge in the direction of film motion to enable high resolving power with time. The width and density of the film image provide a measure of the current magnitude. As current is increased, the film density is "saturated" at increasing distances from the center axis (see Figure 1). However, at a point just beyond this region, the film density can be measured, which together with distance from the axis determines the current magnitude. Current can be measured to an accuracy of about two to one, but over the very great range of 0.1 to 150,000 amperes. The resolving power with time for most of the instruments is 600 microseconds.

Fulchronographs and surge-front recorders were placed at the first seven stations. Photographic surge-current recorders have been added to the stations still in service (see Table I). In addition, a cathode-ray oscillograph and surge integrator are used at the Cathedral of Learning. Figure 2 shows a typical fire-tower station at which surge-crest-ammeter links, the surge-front recorder, the surge integrator, and the photographic surge recorder are used. As shown in the schematic diagram of Figure 2, a fiber tube and fuse wire are placed in the circuit of the surge-front recorder and integrator. This allows the two instruments to record only the first component of a multiple stroke, since their records can sometimes be destroyed by successive discharges.

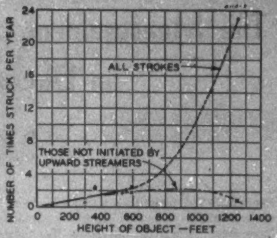


Figure 3. Frequency of strokes to recording stations showing effect of height of object above ground

Solid curve is determined by weighted average of data in Table I

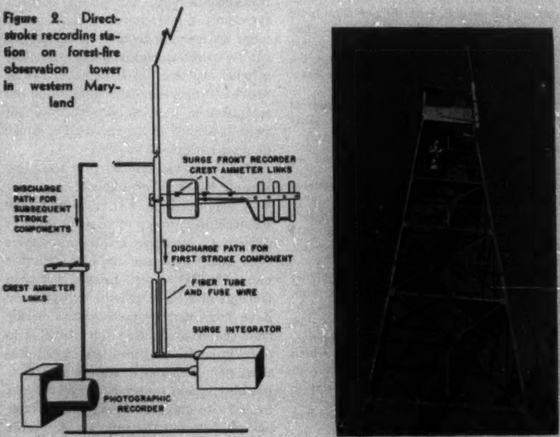
CAMERAS FOR PHOTOGRAPHING LIGHTNING DISCHARGES TO OPEN GROUND

The study of the multiple character of strokes to open ground with ten special cameras[1] has been continued. Seven of these are being used in Western Pennsylvania. Two have been operated by Civil Aeronautics Administration personnel, one by Weather Bureau personnel, and others by C. M. Lear and D. L. Hadley of the Westinghouse Company. D. D. Clarke in Kansas City and L. R. Abraham in Joliet, Ill., have also cooperated in the operation of the cameras and obtained some very interesting photographs.

Number of Records

Forty-nine direct strokes have occurred to the recording stations of which 46 have been measured completely providing data on the magnitude and wave shape of 135 stroke components. The fronts of 33 components have been measured with the surge-front recorders and cathode-ray oscillograph. In addition, six surges with crests ranging from 100 to 800 amperes have been recorded that are considered to have been induced. They have all been of very short duration (50–150

Figure 2. Direct-stroke recording station on forest-fire observation tower in western Maryland



microseconds), and all but one were of positive polarity as would be induced by negative-polarity strokes, the type known to predominate. Photographs have been obtained of 138 strokes to earth with 479 components and 22 cloud-to-cloud discharges having 126 components.

PROBABILITY OF OBJECTS BEING STRUCK

The frequency of strokes to the recording stations is summarized in Table I, together with similar data on three other structures. The effect of structure height on stroke frequency is illustrated by the curves of Figure 3 plotted from these data. In this analysis a segregation has been made between the strokes known by the character of their discharge current to have been initiated by upward streamers from the structure (a stroke mechanism found by McEachron to predominate for strokes to the 1,250-foot Empire State Building),[3] and those initiated by downward streamers from the cloud. As pointed out by Schonland,[4] this latter type of discharge is typical of strokes to open ground or structures of normal height. The distinction between these two types is important, since this causes some of the important variations of stroke current characteristics. Table I and Figure 3 show that about 96 per cent of strokes to the Empire State Building have been initiated by upward streamers while only a small percentage of strokes to structures below 600 feet in height have been of this character. The lowest height for which such a stroke has been recorded is 360 feet. Where strokes of the normal type predominate, Figure 3 shows that stroke frequency is about proportional to height of object. Laboratory model tests[5] indicate that this should be so. The much greater number of strokes to the Empire State Building resulting in the sharp upward trend of the total stroke curve is most likely because upward streamers so frequently occur, contacting charge centers in clouds that otherwise would have produced strokes to ground by another path.

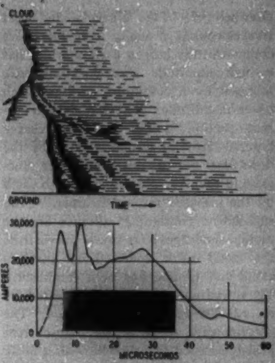


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Figure 5. Cathode-ray oscillogram of first component on record 308A (Figure 4) correlated with probable fluctuations in stroke-channel luminosity as indicated by high-speed Boys camera photographs of Malan and Collens[13] in South Africa

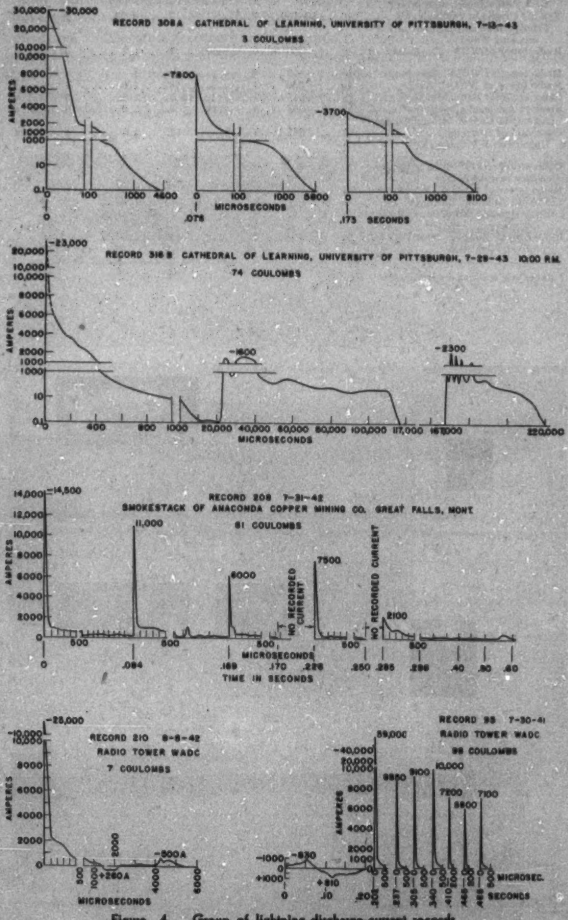


Figure 4. Group of lightning-discharge-current records

Records 308A and 316B measured with fulchronographs and photographic surge recorder, others with fulchronographs alone

Types of Lightning Discharges

Figures 4, 5, and 6 present groups of surge currents illustrating the more typical recorded strokes and several unusual characteristics that have been found. Figures 7 and 8 show lightning camera photographs of particular interest in correlation with the direct measurements of current. The general mechanism of the lightning discharge has been discussed in detail elsewhere.[4,6] The currents in most strokes consist of one or more successive component discharges, each of which has a high-magnitude short-duration peak followed by a continuing flow of much lower magnitude. The initial high-current portion is the result of charge lowered onto the stroke channel by the downward moving initial streamer process and then rapidly discharged to earth by the intense "return streamer." The subsequent continuing currents are the motion of charges more slowly drawn from the cloud after conductivity between cloud and ground has been established. These characteristics are illustrated by records 208 and 308*A* in Figure 4. The general wave shape of the high-current peaks has been found to lie within fairly narrow limits. However, the continuous currents vary widely in magnitude, duration, and method of current fluctuation.

For strokes to tall objects initiated by an upward streamer from the structure, the current starts at low magnitude (not over a few hundred amperes) and may never reach a very high magnitude unless a charge center is tapped in the cloud under such conditions that the stroke channel can be charged by the downward leader process from the cloud and the rapidly discharged by the intense return streamer. All recorded evidence indicates that this is the mechanism required to produce current peaks in excess of a few thousand amperes. Once the stroke channel has been made sufficiently conducting that it can support an increase in current without such a process, the current increase must come from a more rapid drawing of charge from the cloud itself. The magnitude of the continuing currents seldom exceeds 1,000 amperes. The highest-magnitude continuing current so far recorded is that shown in record 316*B* (Figure 4). This indicates the upper range of the rate at which charge can be drawn from the cloud when the stroke channel is highly conducting. Figure 7a and record 141 of Figure 6 show the wide range over which the continuing currents sometimes fluctuate.

CONTINUITY OF CURRENT FLOW

The fact that most records of strokes to the Empire State Building have indicated continuous current flow between current peaks led to the belief that this was a necessary condition for the multiple-stroke mechanism of all discharges.[7] However, the 12 strokes that have been recorded with the highly sensitive photographic surge recorder show that this is not a necessary condition for strokes to

Table I. Direct-Stroke Recording Stations and Their Stroke Frequency

Recording Station and Location	Thunderstorm Days Per Year	Height in Feet	Number of Years	Number of Induced Surges	Number of Direct Strokes	Average Strokes Per Year	Strokes Initiated on Upward Streamer	
							Number	Per Cent
North Wales substation Philadelphia Electric Company	35	80	5		1	0.20		
Eight fire towers in Maryland and West Virginia Maryland State Department of Forestry West Virginia Conservation Commission U. S. Department of Forestry	45	75–125	2		2	0.14	0	0
Ten fire towers in Pennsylvania Pennsylvania Department of Forests and Waters	40	80–100	3		7	0.23	0	0
Radio tower of WHK, Cleveland*	40	300	1		1	1.0	0	0
Radio tower of WCLE, Cleveland*	40	300	1		0	0		
Radio tower of WADC, Akron	40	360	5	2	13	2.6	1	8
Stack at Great Falls, Montana	25	545	4	4	6	1.5	0	0
Stack at Anaconda, Montana* Anaconda Copper Mining Company	30	565	3	2	6	2.0	0	0
Cathedral of Learning University of Pittsburgh	42	585	5		13	2.6	3	24
Other structures for which stroke frequency is available:								
Radio tower of WWSW, Pittsburgh	42	100	3		1	0.33		
Lexington Building, Baltimore Pennsylvania Water and Power Company	35	350	8		3	0.38		
Empire State Building, New York[9,11]	31	1,250	3		68	23.0	About 50 out of 52	96

* Recording stations discontinued.

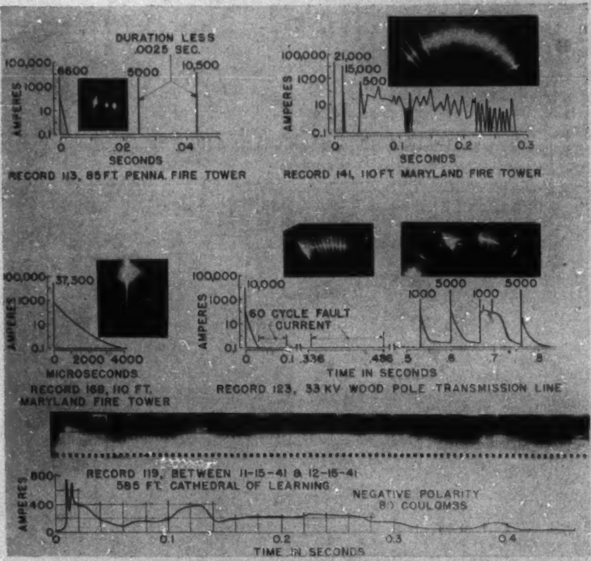
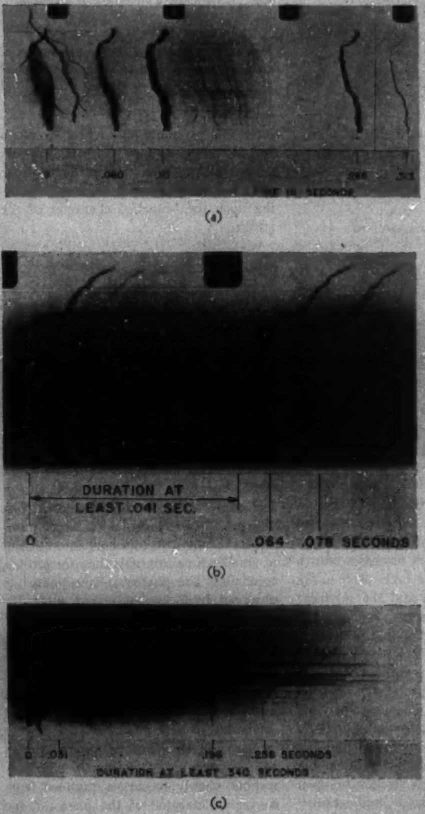


Figure 6. Stroke currents measured with photographic surge-current recorder

Record 119 also recorded by fulchronographs lower objects. Records 308*A* and 316*B* of Figure 4, together with records 113 and 141 of Figure 6, show strokes for which the current decayed to below 0.1 ampere between successive components. Of the 24 recorded multiple strokes, only three have shown uninterrupted current flow to the end of the discharge. These were all to the Cathedral of Learning. They were initiated by upward streamers and contained no high-current peaks. One of these unusual strokes yielded record 119 of Figure 6. It occurred in the winter when there was no evidence of a thunderstorm but probably during a snowstorm. Although the current never exceeded 750 amperes, its long duration (0.46 second) permitted a lowering of 80 coulombs of charge. The lightning camera record of Figure 7c shows that multiple strokes to open ground can consist of a continuous flow of current. This is the only record out of 61 multiple-stroke photographs that shows this condition. However, the recording sensitivity of these cameras is necessarily quite low and highly variable, depending upon visibility conditions. Record 208 of Figure 4 shows continuous current flowing between several of the high-current peaks. This record shows that the distinct high-current peaks can occur when the channel is carrying an appreciable current even though the "dart leader–return streamer" process is required.

Figure 8 illustrates a condition that is only known to have been recorded in these two photographs of strokes occurring during the same storm. They show a series of higher luminosity pulses superimposed on continuous luminosity of low intensity. The time intervals are very regular deviating only slightly from 0.01 second. This is in contrast to the random distribution of time intervals found for the distinct components of multiple strokes. This condition has been simulated with laboratory arcs in dielectric recovery studies.[8] It is intermediate between the condition of continuous current flow with no high-current peaks and the more common type of multiple stroke with distinct components. For the first of these sufficient channel conductivity is maintained that any increase in the rate of cloud charge generation or tapping of charge by streamers within the cloud is immediately accompanied by a corresponding increase in current flow. Fluctuations are, therefore, relatively gradual. However, the dielectric recovery studies have shown that even with some current flowing the channel (at sufficiently low-current density) will offer very high impedance to any increase in current until the voltage across it rises to a definite critical value. At this voltage a new discharge is precipitated. This critical voltage is indicated to be low enough when currents of a few amperes are flowing that it could be exceeded in 0.01 second intervals by the rates of cloud charge generation indicated by ground gradient measurements.[6,9] It is thus probable that the pulses are produced from the discharge of the same limited region of the cloud (charging up at a fairly uniform rate) and are not the result of tapping new discrete charge centers that produce the normal type of multiple component.

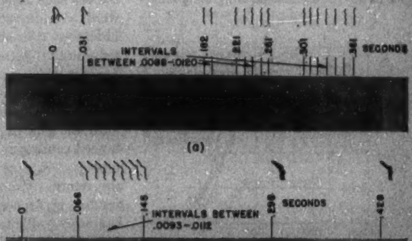


(a)

(b)

(c)

Figure 7 (left). Lightning-camera photographs of multiple strokes to open ground with continuing type of current flow



(a)

(b)

Figure 8 (below). Lightning-camera photographs showing successive current pulses occurring at regular time intervals and superimposed on lower-magnitude continuous current flow

POLARITY

Most strokes to ground have been found to involve only the negative charge[4,6] generated in the cloud base.

Of the 46 recorded strokes in this investigation, 41 or 80 per cent have shown entirely negative current flow to earth. McEachron has published data showing that 84 per cent of the strokes to the Empire State Building were entirely negative.[3,11] In five records of the Westinghouse investigation some positive charge has been lowered. However, for all but two negative current flowed initially. Thus, 96 per cent have been initiated in the negative charge centers near the cloud base. The remaining two strokes were entirely positive. As pointed out in reference 9, such strokes seem most likely to be initiated from the positive charge centers of limited extent occurring in the lower regions of the cloud. However, photographic records have been obtained of two strokes tapping charge from the positive charge centers in the upper regions of the cloud.[10] One of these, which was recorded in this investigation, showed a multiple stroke, each component of which tapped charge from the top of the cloud.

HIGH-FREQUENCY FLUCTUATIONS IN HIGH-MAGNITUDE SHORT-DURATION PEAKS

The cathode-ray oscillogram of Figure 5, which recorded the initial component of record 308*A* (Figure 4), shows an interesting current condition. After reaching its initial crest, the current rapidly decayed to about 70 per cent of this magnitude and then rose rapidly to an even greater crest. On the initial front of the wave the average rate of current rise (as defined by a straight line through the 10 and 90 per cent points is 9,000 amperes per microsecond with a maximum rate of rise of about 15,000 amperes per microsecond. Maximum rates of current change of this same order of magnitude occur in the subsequent fluctuations. Malan and Collens[12] have photographed with a high-speed Boys camera corresponding rapid fluctuations in the luminosity of the stroke channel for strokes having branches. A schematic record, such as they have obtained, is drawn above the oscillogram of Figure 5. Their records show that at the start of the upward "return streamer" the channel luminosity rises abruptly and remains intense until the streamer reaches a branch point at which a pause takes place in the streamer propagation and the intensity of the channel luminosity falls off sharply until the charge in the first branch is tapped causing an abrupt increase. The tapping of successive branches as the return streamer propagates upward results in successive pulses of this character. Photographs published by McEachron[3] of Empire State Building strokes show relatively little branching as compared to many strokes to open ground. Also, his current oscillograms[11] have less pronounced fluctuations than record 308*A* (Figure 5). It appears probable that such current fluctuations should occur to varying degrees, depending upon the branched character of the stroke.

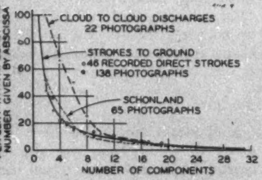


Figure 9. Percentage distribution curves of the number of components in lightning strokes

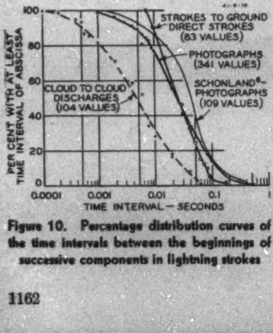


Figure 10. Percentage distribution curves of the time intervals between the beginnings of successive components in lightning strokes

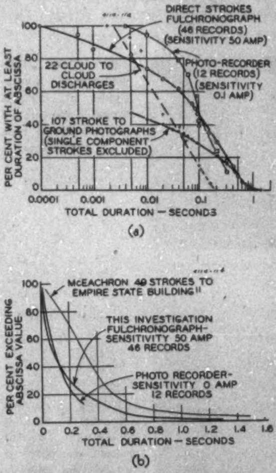


Figure 11. Percentage distribution curves of the total duration of lightning strokes

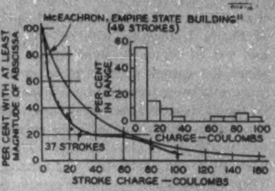


Figure 12. Percentage distribution curves of the total charge in lightning strokes

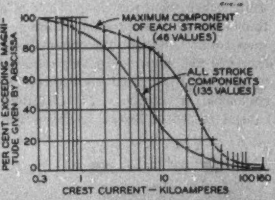


Figure 13. Percentage distribution curves of crest current in individual components of lightning strokes

Statistical Analysis of Strokes

An analysis has been made of the more important discharge current characteristics to determine their range of variation and the probability of their occurrence.

MULTIPLE CHARACTER AND TOTAL DURATION

In Figures 9 to 11 are given probability curves on the number of stroke components, their time intervals, and total stroke duration. Curves are also shown for the recorded cloud-to-cloud discharges. These consist of a larger number of component discharges occurring at smaller time intervals than for strokes to earth. The photographs indicate (as observed by Schonland) that such discharges are merely a series of "dart" leaders that could not last more than a few microseconds nor have current peaks exceeding a few thousand amperes.[9] Quite close correlation is shown for the number of components and their time intervals, as recorded in the direct strokes and the photographs of strokes to open ground. These curves also correspond closely to curves obtained from a similar analysis[6] of the photographic

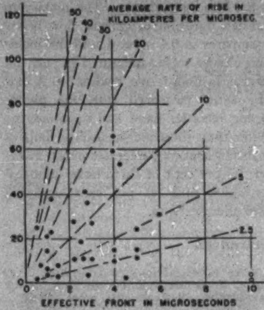


Figure 14. The 33 wave-front records showing relation to crest magnitude and average rate of current rise

Effective time to crest for records of this investigation is defined by straight line through 10 and 90 per cent points

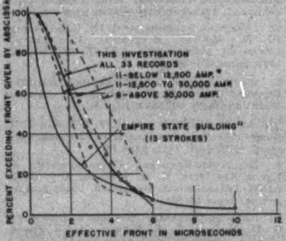


Figure 15. Percentage distribution curves of stroke current wave fronts

* Two fronts of very low-magnitude components are excluded

records of Schonland taken in South Africa.[4] Sufficient data have not yet been obtained to make such a statistical analysis for each direct-stroke station. However, the segregation of the photographs obtained in the Pittsburgh area from those of Kansas City and Joliet show no marked trend from the plotted curves based upon total records. This indicates a similarity between thunderstorm conditions in different parts of the United States and the world.

On the other hand, there is definite evidence, both from the camera records and visual observation, that the multiple character varies considerably with different storms. In some storms strokes consist predominantly of single component discharges. These appear to be storms of relatively small electrical activity occurring when meteorological conditions are less favorable for charge generation. For these cases the charge centers are probably less numerous and of smaller extent. In other storms the strokes are predominantly multiple. There is also evidence that the amount of branching for strokes to earth is affected by terrain. Over the flat, bare terrain around Joliet most recorded strokes have shown little or no branching, while over the hilly, wooded terrain in Pennsylvania most recorded strokes have shown profuse branching. It is to be expected that prominent irregularities on the earth's surface can produce enough effect on the field at the tip of the pilot streamer as it proceeds to ground to form branches toward these points on the ground.

The variations between the three total duration curves for strokes to earth in Figure 11a are thought to be due entirely to differences in recording sensitivity. The photographic data for strokes to open ground should show the shorter durations and the highly sensitive photographic surge-current recorder the longer durations. This effect is quite pronounced at the lower range of durations. At the longer durations, where the effect of not recording the total duration of the last component has less effect, the curves lie quite close together. The definite effect of the very tall Empire State Building in prolonging the stroke duration is shown in Figure 11b.

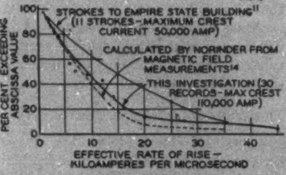


Figure 16. Percentage distribution curves of rates of current rise as defined by straight line between 10 and 90 per cent points on wave front

Total Stroke Charge

Probability curves for the total charge lowered to earth by strokes are given in Figure 12. Except for discharges of few components and short duration, resulting in the lowering of small amounts of charge, the great preponderance of the total charge lowered is during the flow of the continuing current of low magnitude. Although a current peak may have a magnitude as high as 100,000 amperes, its duration is too short to involve more than a few coulombs. In the figure the data of this investigation are compared with those obtained by McEachron. This shows that a greater percentage of strokes of the normal mechanism lower less total charge than recorded for the Empire State Building. This is probably because the field distortion of the building tends to maintain the channel conductivity for a longer period of time, resulting in the continuing form of discharge and the tapping of a greater percentage of the cloud charge. Figure 12 indicates that the principal range of lowered charge lies between 5 and 100 coulombs with most strokes involving less than 10 coulombs.

Crest Currents

Figure 13 shows the relative occurrence of crest current magnitudes. Curves are plotted both for all components recorded and for only the maximum components of each stroke. The maximum current recorded is 160,000 amperes. About 75 per cent of the crest currents in the maximum component exceeded 10,000 amperes, while about 20 per cent exceeded 40,000 amperes.

Wave Fronts

In this investigation one direct stroke and 14 arrester discharges have been

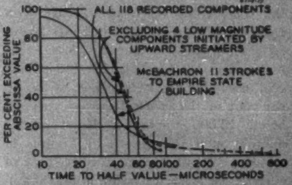


Figure 17. Percentage distribution curves of times to half value of high current peaks in individual components of stroke currents

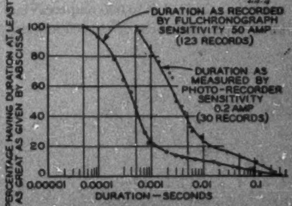


Figure 18. Percentage distribution curves of duration of individual components of stroke currents

Where current flows continuously between components time is measured to start of succeeding component simultaneously recorded with the cathode-ray oscillograph and the magnetic surge front recorder. The maximum difference between the fronts, as measured by the two instruments, is 20 per cent, showing that a good degree of accuracy is provided by the latter instrument. In Figure 14 the 33 measured wave fronts are plotted showing effective times to crest in relation to crest magnitude and average rate of current rise. In Figures 15 and 16 are probability curves obtained for the wave front and average rate of rise. All fronts so far recorded lie between one-half and ten microseconds. As shown by Figure 15, most strokes had longer fronts than found by McEachron to the Empire State Building. Only 40 per cent of the records of this investigation showed fronts less than two microseconds, while he found 50 per cent less than one microsecond. The probability has been pointed out previously[13] that the presence of such a tall structure should affect the wave front causing it to be shorter than for strokes to low objects.

To obtain some indication of the relation between crest magnitude and wave front, the records were divided into three groups of approximately equal number and the three dashed probability curves of Figure 15 plotted. Although there are not yet sufficient data to determine a definite relation, these curves show that the wave front tends to increase with crest current. For crest currents above 30,000 amperes, the shortest front so far recorded is 1.2 microseconds and 80 per cent exceed 2.5 microseconds, while 70 per cent of the fronts for crest currents below 12,000 amperes is less than 2.5 microseconds. As shown by Figures 14 and 16, the maximum average rate of current rise so far recorded is 45,000 amperes per microsecond, which occurred for a component having a peak current of only 25,000 amperes. The highest magnitude component for which the front has been recorded (110,000 amperes) had an average rate of rise of 40,000 amperes per microsecond. The comparison with the Empire State Building data in Figure 16 shows the trend of lower rates of current rise for strokes to lower objects. The only other known data available on the wave fronts of direct strokes are those of Norinder plotted in Figure 16. His data are, however, questionable, as they were estimated from measurements made of the magnetic fields produced by strokes.

Times to Half Values

Probability curves on the time to half value of the high current peaks are given in Figure 17. Excluding four low-magnitude components of strokes to the Cathedral of Learning that were initiated by upward streamers, the maximum time to half value so far recorded is 90 microseconds. The data of this investigation show a smaller percentage of times to half value in the lower range than recorded by McEachron. However, such values as recorded by the fulchronograph are not very accurate below 20–30 microseconds. It is significant that an appreciable number of times to half value greater than that of the standard 1$^{1}/_{2}$x40-microsecond test wave are recorded. Such long tail surges will impose more severe duty on certain types of system insulation and influence the insulation co-ordination problem.

Duration of Individual Components

Figure 18 gives probability curves comparing the durations for individual stroke components as measured by the fulchronograph and the more sensitive photographic surge-current recorder. It is of interest that for any percentage occurrence value on this figure the duration as recorded by the fulchronograph is only a small fraction of that shown by the photographic-recorder curve. This shows that on the average the duration of current above 50 amperes in a component is only a small fraction of its total duration. This, however, has little effect on the value of total charge as measured by the two instruments. The maximum recorded duration of current flow below 50 amperes is 0.1 second, and the maximum charge so far measured in such an interval is about one coulomb. A few components measured with the photographic recorder have had durations no greater than the time sensitivity limit of the instrument (600 microseconds). However, 95 per cent of them have exceeded this value and 50 per cent exceeded 3,000 microseconds.

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Capacitor Installation at Newport News

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IN the fall of 1942, load predictions, based on unprecedented expansion of war activities, showed that existing generating and transmission facilities in the Virginia Public Service Company's eastern division could not supply the kilowatts and kilovars required for the spring of 1943. It was found that no kilovars would be available from the source of transmission to the area but that the kilowatt requirements could be met from the existing generating and transmission sources. It was evident that a new source of reactive current would have to be supplied.

Estimates, based on the reactive component of the predicted load, plus the calculated kilovar losses resulting from I^2X in the inductance of the transmission line and transformers, showed the need for approximately 15,000 kilovars.

Solution of the Problem

Preliminary Considerations

The question then arose as to whether the kilovars should be supplied from static capacitors or from a synchronous condenser. An analysis showed that static capacitors had the following advantages:

1. Quicker delivery—3 to 6 months for static capacitors contrasted with 12 months for synchronous condensers.

2. Less critical materials—the aluminum foil used in static capacitors is rated lower than that required for prime war purposes.

3. Lower cost including capitalized losses—approximately $7.04 per kilovar for static capacitors against $11.89 per kilovar for a synchronous condenser based on its nameplate rating, or $7.93 per kilovar if the capacity is based on 150 per cent of nameplate rating considering its 50 per cent lagging capacity. It should be pointed out here that lagging capacity can also be supplied by adding an autotransformer and shunt reactor to the capacitor bank and at a considerably smaller cost per kilovolt-ampere than the original capacitors.

4. Greater flexibility—after the present emergency, static capacitors can be redistributed as needed; the synchronous condenser would have to remain in bulk.

5. Lower losses—in a location where losses are particularly important. Static capacitors have about one third of one per cent loss against 2.1 per cent for a synchronous condenser.

6. Static capacitors would not add to the interrupting duty on oil circuit breakers whereas a synchronous condenser would.

7. Static capacitors can be switched in or out in blocks as desired without objectionable switching effects. This can be done either manually or automatically quite satisfactorily. The ability to control kilovars in desired quantities is not peculiar to static capacitors, of course, as a fine degree of such control is practical with a synchronous condenser.

In this comparison, the following factors were considered in favor of the synchronous condenser:

1. It can generate reactive current up to 100 per cent of its rating and, in addition, absorb reactive current up to 50 per cent of its rating. If this absorption capacity can be used at times of light load, it has an effective corrective capacity of 1.50 times its rating.

2. A synchronous condenser will reduce flicker caused by a large fluctuating load.

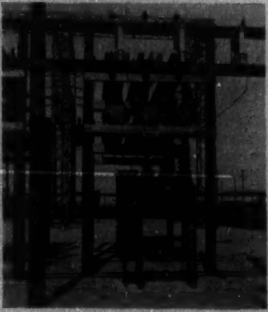
3. A synchronous condenser will increase the stability limits of the system.

Having decided upon static capacitors as the means of supplying kilovars, the following questions remained to be answered:

1. What size units should the capacitors be?

2. What voltage rating should the capacitors have?

3. What size banks should they be switched in?



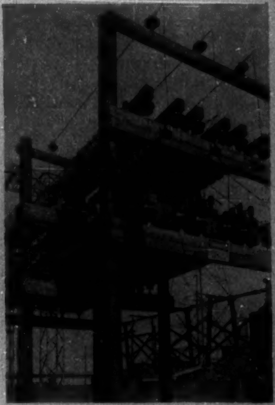
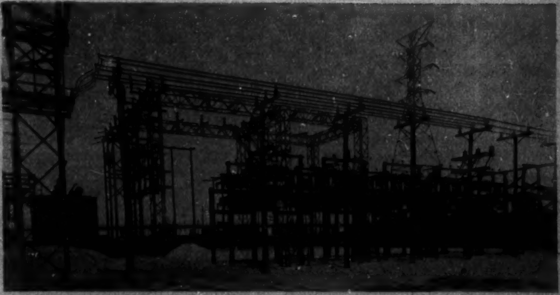


Figure 1

(a, above). General front view of 2,700-kilovar bank 5

(b, right). Detailed front view of bank 5

(c, below). Front view of banks 1, 2, 3, 4, and 5



Paper 44-117, recommended by the AIEE committee on power transmission and distribution for presentation at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944. Manuscript submitted March 22, 1944; made available for printing May 19, 1944. The substance of this paper also was presented at the AIEE Southern District technical meeting, Roanoke, Va., November 16–18, 1943.

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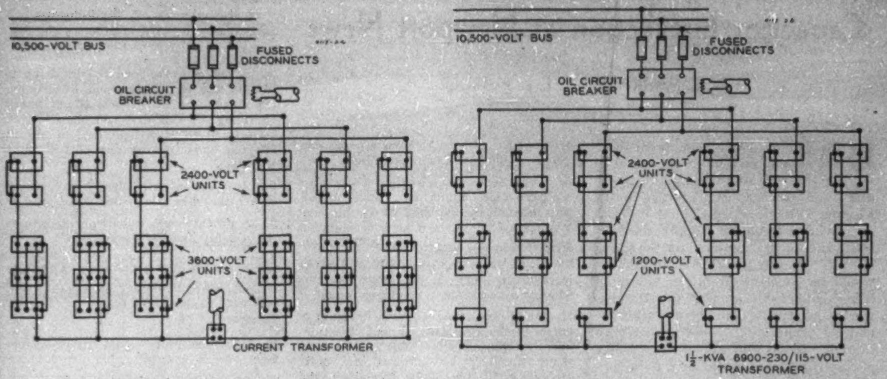

Figure 2. Schematic diagrams of capacitors

All units rated at 15 kilovars. Each capacitor shown represents six units. Timbers on which capacitor units are mounted are supported on 15-kv insulators

(a, left). Using current relay between neutrals. Three banks utilize 2,400- and 3,600-volt units

(b, right). Using voltage relay between neutrals. Two banks utilize 2,400- and 1,200-volt units

4. Should they be rack-type housed either indoor- or outdoor-type units, complete with fuses for individual capacitor units and indoor oil circuit breaker?

5. Should they be outdoor pole-mounted type, hung on a wooden structure to be assembled in the field and controlled by an outdoor oil circuit breaker?

Design Considerations

Generally, distribution primaries in the Newport News area are at a nominal voltage of 3,450 volts. At the time of this analysis, about 2,025 kilovars of capacitors had been installed in small groups at scattered points on the 3,450-volt primaries and 270 kilovar were in service at 2,300-volt industrial stations. It was found that 1,260 kilovars in 15-kilovar 2,400-volt pole-type units could be connected through fuses in 180-kilovar banks to the four-kv bus of a transmission substation under construction without the necessity for capacitor switching.

The principal transmission substation at Newport News changes the incoming 110,000 volts to 10,500 volts. This is an attended station and it was decided that 13,500 kilovars of capacitors should go directly on the 10,500-volt bus. When the present emergency is over, it will be desirable to reinstall many of these units at other points on the system, principally at 3,600 and 2,400 volts. The desirability of standard outdoor-type pole-mounted units applicable to either 3,600- or 2,400-volt systems was evident. A comparison of costs of the types considered above likewise favored the standard outdoor pole-mounted type provided the cost of switching could be held to a minimum. Table I shows the relative costs of capacitors of different voltage classes.

It was desired to hold the voltage change per switched bank to approximately 1.75 per cent. It was not desired to have too large a portion of the entire installation affected by the failure of a single 15-kilovar unit, both from the standpoint of ease of locating the failed unit and of having all the units on a given oil circuit breaker ineffective while the failed unit was being located and replaced. Because of the connections involved, the size of the bank would have to be selected in terms of 450-kilovar increments. These several reasons resulted in the selection of 2,700 kilovars as the size of the desired switching unit. Each 2,700-kilovar unit is connected to the bus through deion fuses for short-circuit protection and provided with oil circuit breakers for normal switching.

Description of Installation on 10,500-Volt System

The capacitors are connected in ungrounded wye on the 10,500-volt bus in banks of 2,700 kilovars through individual bank breakers tapped to the bus through fuses. Figure 1 shows typical installation views. All capacitors are 15-kilovar outdoor pole-type units. There are a total of 900 such units totaling 13,500 kilovars, 324 such units or 4,860 kilovars are rated at 3,600 volts, 504 units or 7,560 kilovars are rated at 2,400 volts, and 72 units or 1,080 kilovars are rated at 1,200 volts. The 1,200-volt units will ultimately be used two in series on 2,400-volt systems.

Each bank of capacitors consists of two wye-connected groups as shown diagrammatically in Figure 2. The neutrals of the two wye groups are connected together through a current or potential transformer which is in turn connected to a current or a voltage relay which actuates the trip circuit of the bank breaker. Thus the failure of a capacitor unit causes a displacement of the neutral of one wye group with respect to its companion group and the resulting current or voltage causes the breaker to trip, deenergizing the bank. Three of the banks have current control, while two of them have voltage control.

Three of the banks consist of 3,600- and 2,400-volt units. Since the voltage to neutral of the 10,500-volt system is approximately 6,000, it is seen that the two types will work together in series if the current through the series combination is the same. The normal current for a 15-kva 2,400-volt unit is about 6.3 amperes and for a 3,600-volt unit only 4.2 amperes. It is thus seen that for every two 2,400-volt units in multiple, there must be in series three 3,600-volt units. That is, the ratio of the number of 2,400-volt paralleled units to be connected in series with 3,600-volt paralleled units must be as two is to three.

Two of the banks consist of 1,200-volt and 2,400-volt units, two sets of 2,400-volt units and one 1,200-volt unit being connected from line to neutral in this case. For a reason similar to that described above, two 2,400-volt paralleled units are required to operate in series with a single 1,200-volt unit.

To minimize the danger of bus faults from birds, cats, and so forth, weatherproof wire was used along with insulated terminal caps for connecting the units.

The bus to which the capacitor banks are tapped consists of two two-inch galvanized iron pipes, slotted in order to minimize heating. The bus is supported by a single wood-pole line as can be seen from Figure 1.

Operating Results—General

The capacitors have been in service about 17 months to date. All banks are in operation part of the day and none is in operation during light-load periods.

The performance to date of the capacitors has been as expected and, in the main, altogether satisfactory.

The failure of a unit is indicated by the tripping of the oil circuit breaker. A careful inspection of the shape of the cases of the units in the bank will generally show a bulge or distortion indicating a failed unit. In case the failed unit is not evident, the bank is energized and tripped again and another inspection is made.

During the first six months operation, a total of five units were lost, four being rated at 2,400 volts and one at 3,600 volts. This seems to bear out other experience on large banks that the first few months operation forms a weeding out period in which the defective units show up.

A surprisingly audible humming of the iron-pipe bus accompanied the passage of the capacitor current. The slotted pipe acted as a tuning fork and was found to be resonating at 480 cycles. Relatively simple remedial measures can be employed if found necessary, such as tack welding the slot at intervals.

Since the substation at which the capacitor is located is attended at all times, the units are switched on and off by manual control from the switchboard inside the control house. Automatic operation of an oil circuit breaker, caused by the failure of a unit, is indicated by alarm to the operator.

Voltage regulation and other operating performance characteristics are discussed under "Tests."

The actual cost per kilovar of the entire 13,500-kilovar bank, including switching, was $6.25 and is detailed in Table II.

Table I. Approximate Relative Cost Per Kilovolt-Ampere for Capacitors

Voltage	Approximate Relative Cost	Approximate Cost With Outdoor Rack, Per Kva
230	2.38	$12.60
460	1.19	6.30
2,400	1	5.30
4,160	1	5.30
4,800	1.12	5.95
7,200	1.19	6.30
13,800	1.50	7.95

Table II. Cost of Capacitor Installation Per Kilovar

Capacitor units delivered	$4.58
Oil circuit breaker delivered	.45
Miscellaneous other material delivered	.51
Labor and transportation	.33
Insurance, taxes, interest and stores overhead	.38
Total	$6.25

Tests

As a result of the size of the installation and certain novel features involved, it was decided to conduct tests to determine its operating characteristics and its effect on the system. The 13,500-kva capacitor is switched manually in five equal 2,700-kva sections and the effect of switching in or out various sections was observed. At the same time the effects on the harmonics of the system were observed for various amounts of connected capacitor kilovolt-amperes. It was expected that the tests on this capacitor would further confirm the correctness of engineering applications in large banks and thereby pave the way for further installations of this kind. Field tests to date were comparatively few and there were enough differences in this application compared with previous ones to warrant such an investigation.

In general, the results have fully confirmed the correctness and adequacy of the installation and have confirmed the ability to predict from theoretical considerations the regulation characteristics, the inrush currents, the voltages during switching, and the effect of the capacitors on system wave shape. The tests will be discussed under these various headings.

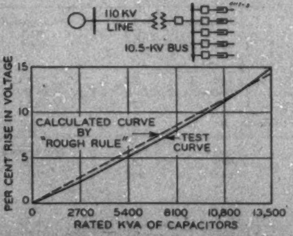

Figure 3. Voltage rise due to capacitors at the end of the Richmond line, with no other load

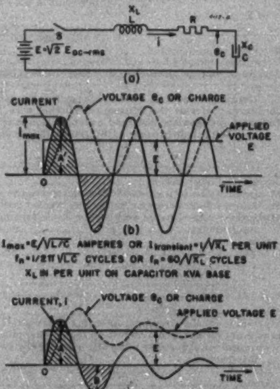
X_L
L
R
S
i
$E = \sqrt{2}\, E_{ac-rms}$
e_c
X_c
C
(a)
CURRENT
VOLTAGE e_c OR CHARGE
I_{max}
APPLIED VOLTAGE E
A'
E
0
TIME
(b)
$I_{max} = E/\sqrt{L/C}$ AMPERES OR $I_{transient} = 1/\sqrt{X_L}$ PER UNIT
$f_n = 1/2\pi\sqrt{LC}$ CYCLES OR $f_n = 60/\sqrt{X_L}$ CYCLES
X_L IN PER UNIT ON CAPACITOR KVA BASE
CURRENT, I
VOLTAGE e_c OR CHARGE
APPLIED VOLTAGE E
A
E
0
TIME
(c)

Figure 4. Inrush current when switching a capacitor

REGULATION

The subdivision of the capacitor bank into five separately switched units was based primarily on keeping the voltage steps within 1.75 per cent when a unit was switched on or off with two substation transformer banks in service or within two per cent with one transformer bank in service.[4] A rough rule in common use for small voltage increments may be stated as follows: "The per unit voltage change when a capacitor is switched on or off equals the ratio of capacitor kilovolt-amperes to system short-circuit kilovolt-amperes at that location." This rule is a crude approximation based on the argument that since the lagging short-circuit kilovolt-amperes lowers the voltage to zero, then one per cent as much leading kilovolt-amperes should raise the voltage one per cent. It assumes a pure reactance system and ignores the difference between synchronous and transient reactance of generators, or the action of voltage regulators at generator busses, and the nonlinear variation of load and transformer magnetizing currents with system voltage. Consequently, it is of value to determine experimentally what voltage variation is actually obtained per capacitor step.

A three-phase short circuit at the capacitor location draws 135,000 kva with one substation transformer bank in service. On this basis using the rough rule just cited each 2,700-kva section added should raise the voltage about two per cent. However, the actual voltage change encountered is only one per cent per step, or exactly half of that obtained by the rough rule. The importance of this is self-evident since the cost of switching increases with the number of sections involved.

This reduction in voltage increment is due chiefly to the self-regulating effect of loads and transformer magnetizing, and may not be obtained in situations where these effects are unimportant. For example, the capacitor was connected through a transformer to the receiving end of the long transmission line from Richmond, without other load at the receiving end. The short-circuit kilovolt-amperes in this case is 60,000. It might be reasoned from the rough rule that the complete 13,500-kva bank should raise the voltage 29 per cent.* Also, since the transformer reactance was just half of the total reactance, the voltage rise at the end of the transmission line would be 14.5 per cent. Figure 3 shows the actual

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*The 0.29 is computed from

$$\text{Voltage increment} = \frac{\text{capacitor kilovolt-amperes}}{\text{short-circuit kilovolt-amperes} - \text{capacitor kilovolt-amperes}}$$

$$= \frac{13{,}500}{60{,}000 - 13{,}500}$$ which reduces to the rough rule for small increments

measured rise at the end of the line as a function of the rated kilovolt-amperes of capacitors added. It will be noted that in this case the actual rise is practically equal to that given by the rough calculation.

The empirical reduction of voltage increment due to system self-regulating effects should therefore be applied only to similar situations.

INRUSH CURRENTS

It is important to know the maximum inrush current to determine the adequacy of circuit breakers, current transformers, fuses, or other devices which may be in the circuit. While the phenomenon of switching a capacitor has been described previously, it will be beneficial to review briefly the action which takes place in order to provide a background whereby the results of the tests may be better understood.

In order that the figures cited may provide a clear sense of the magnitudes involved, all currents will be expressed as a number of times the rated current of the capacitor that is being switched on. Similarly, all voltages will be given as a number of times the normal line-to-neutral voltage of the system. Thus the line-to-line voltage of the system is 1.73.

Fortunately, the maximum inrush currents of three-phase capacitor banks are the same as the inrush current of one line-to-neutral phase alone. This is obvious in the case of a grounded-star bank where each phase acts independently and where the transient inrush current therefore can be determined by considering one star phase alone. It will be shown later that with star-ungrounded banks and delta banks the maximum inrush currents are the same as though the bank were star grounded. This convenient fact will be used now and demonstrated later. Thus only one single star phase of the capacitor need be considered for the time being.

When a capacitor is first connected to an a-c source a transient charging current flows of large magnitude but short duration. The important part of this high-frequency transient current is usually all over in a small part of a fundamental cycle. Consequently, the generated 60-cycle voltage of the supply does not change materially during the short time involved. It is, therefore, convenient to picture the phenomenon as a direct voltage applied, of magnitude equal to the instantaneous value of the alternating voltage at the time that the switch is closed. The largest transient current occurs if the switch is closed at the crest of the alternating-voltage wave and hence a direct-voltage of this magnitude must be considered in order to determine the maximum transient current.

Suppose then that a direct voltage is applied to a capacitor C, through a lead inductance L, as shown in Figure 4. If the resistance were zero, the transient current would be a sine wave of high frequency as shown in Figure 4b. Resistance causes it to decay rapidly as shown in Figure 4c, and the capacitor voltage approaches the applied voltage in an oscillatory manner as shown dotted. However, the resistance can usually be neglected with but small error in determining the first crest of transient current and the expression for transient current will therefore be given on this basis. Later, the small correction for decay will be considered.

During the first half cycle of transient current, crosshatched in Figure 4b, charge is carried into the capacitor, charging it up to twice the applied voltage. The reason for this overshoot is the inductance, which continues the current flow into the capacitor after it has charged up to the applied voltage.

The classical expression for crest current magnitude is

$$i_{max} = E/\sqrt{L/C} \qquad (1)$$

However, a stratagem will again be employed to put this expression in a form that means more to the senses. In the first place, the current, which is a sine wave, albeit of high frequency, will be expressed as a number of times the normal 60-cycle current of the capacitor being switched on. Secondly, the 60-cycle reactances, X_L and X_C, will be used in place of L and C, and these will be expressed in per unit (per cent divided by 100) on the capacitor kilovolt-ampere base. When this is done the maximum transient current is given as

$$I_{transient} = 1/\sqrt{X_L X_C} \text{ per unit} \qquad (2)$$

For the particular case shown in Figure 4a, the capacitor reactance is

$$X_C = 100 \text{ per cent} = 1.0 \text{ per unit}$$

since any shunt capacitor has 100% reactance on its own kilovolt-ampere base. For this case the transient current expression reduces to

$$I_{transient} = 1/\sqrt{X_L} \text{ per unit} \qquad (3)$$

Table III. Fundamental-Frequency Voltages to Ground and Across Breaker Contacts When Disconnecting Three-Phase Capacitors Connected Delta or Ungrounded Star

All Voltages Are Expressed in Per Cent of Normal Line-to-Neutral Voltage Crest. It Is Assumed That the Supply System Is Grounded and Does not Suffer Neutral Displacement While Switching Capacitors

A. Normal opening. One phase current is interrupted at current zero and 90 degrees later the other two phase currents are interrupted simultaneously at current zero

Phase	A	B	C
Sequence of interruption	First	Second	Second
Maximum voltage to ground following interruption	150	87	87
Maximum voltage across corresponding breaker pole	250	187	187
Maximum voltage across a capacitor leg following interruption:			
Star	100	37	137
Delta	*A–B* 63	*B–C* 173	*C–A* 237

B. Controlled opening. Phases interrupted many cycles apart. Simulated by starting with B phase open and A pole of the breaker by-passed

Phase	B	C	A
Sequence of interruption	First*	Second	Not opened
Maximum voltage to ground following interruption		273	
Maximum voltage across corresponding breaker pole		346	

C. Controlled opening. One phase current is interrupted at a normal current zero. The second opens 90 degrees later at the subsequent current zero. But the third pole is by-passed and does not open. Different voltages are obtained depending on whether the first two poles open in sequence B—C or C—B. With close breaker adjustment either is equally probable and both were obtained in tests

Phase	B	C	A
Sequence of interruption	First	Second	Not opened
Maximum voltage to ground following interruption	337	273	100†
Maximum voltage across corresponding breaker pole	410	346	200†
Maximum voltage across a capacitor leg following interruption:			
Star	100	37	137
Delta	*B–C* 63	*C–A* 173	*A–B* 237

Phase	B	C	A
Sequence of interruption	Second	First	Not opened
Maximum voltage to ground following interruption	273	163	100†
Maximum voltage across corresponding breaker pole	346	236	200†
Maximum voltage across a capacitor leg following interruption:			
Star	137	100	37
Delta	*B–C* 237	*C–A* 63	*A–B* 173

* Open at start of test.

† If opened. Assumes capacitance coupling to ground holds capacitor potential fixed after last pole opens.

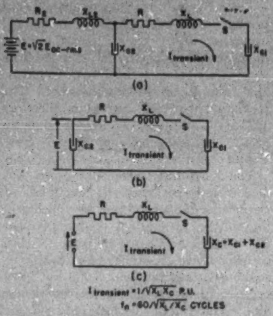

Figure 5. Capacitors switched in parallel

The curve of Figure 6 has been drawn representing this relationship. It shows that with ten per cent source reactance the inrush current would be 3.16 times the normal capacitor current. With smaller source reactances of one per cent, 0.01 per cent, or 0.0001 per cent the inrush current is 10, 100, or 1,000 times normal.

Thus the source reactance up to the capacitor can be computed in the usual manner, expressed in per cent or per unit on the kilovolt-ampere base of the capacitor being switched on, and the maximum transient current readily determined.

In the Newport News installation the reactance up to the capacitor is just ten per cent on the 13,500-kva base. Consequently, if it could be switched as a unit, the maximum inrush current would be 3.16 times normal. A special setup was made whereby the entire bank could be energized over the Richmond line by closing the transformer breaker. In this case the reactance was 22.6 per cent on the 13,500-kva base. The maximum theoretical inrush current from equation 3 is 2.1 times normal. The inrush shown by the oscillogram was approximately twice normal. Much larger inrushes occur when switching capacitors in parallel as will be shown presently.

Frequency of Transient Inrush Current

It is sometimes desirable to know the frequency of the inrush current. For example, in the tests the current transformers sparked over. It was found that the special inductances used with test instruments had so much reactance at this high frequency that excessive voltages were developed, and it was necessary to short-circuit these coils while switching capacitors in parallel. From a knowledge of the current magnitude and frequency the burden voltage can readily be calculated.

The classical expression for natural frequency of the transient in the circuit of Figure 4a is

$$f_n=\frac{1}{2\pi\sqrt{LC}}\text{ cycles} \tag{4}$$

For the 60-cycle system with reactances expressed in per unit this reduces to

$$f_n=60/\sqrt{X_L/X_C}\text{ cycles per second} \tag{5}$$

and for the special case of Figure 4 in which $X_C=1.0$ per unit this becomes simply,

$$f_n=60/\sqrt{X_L}\text{ cycles per second} \tag{6}$$

Note that for Figure 4 the frequency is the same number of times normal as the current. It is 600, 6,000, or 60,000 cycles for reactances of one per cent, 0.01 per cent, or 0.0001 per cent, respectively, as shown in Figure 6.

Capacitors Switched in Parallel

When one capacitor is already energized and a second is switched in parallel with it, there are usually two rather distinct transients. The reactance X_L, between the switched sections in Figure 5a, is usually very small compared with reactance X_{LS} back to the source. Hence, a high-frequency equalizing transient with large currents takes place between sections, followed later by a much slower low-current transient between the complete bank and the source. The latter is of small importance, and the high-current transient only will be considered. For this purpose the system may be ignored as in Figure 5b. The current in this circuit is the same as in Figure 5c which is the same as Figure 4 except that the capacitive reactance now includes the two reactances in series. The reactance X_{C1} is 100 per cent or 1.0 per unit. It is necessary to express the reactance of the capacitor already energized, X_{C2}, in per unit on the kilovolt-ampere base of the one being added. These two reactances are added to obtain the total capacitive reactance, X_C.

For example, if the capacitor added is equal in kilovolt-amperes to that already energized, then $X_C=2.0$ per unit. If four units are on and a fifth is being added, $X_C=1.25$ per unit. With this interpretation the transient current magnitude in per unit of the normal current of the capacitor added is given by equation 2 and Figure 6 and the frequency by equation 5 and Figure 6.

Laboratory tests of paralleling two 75-

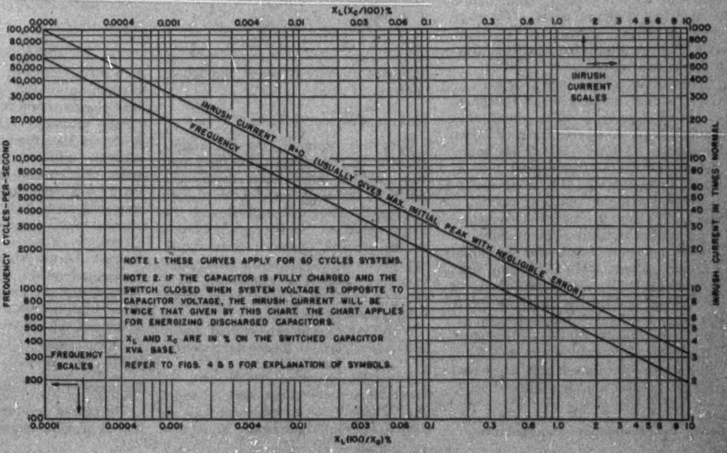

Figure 6. Magnitude and frequency of transient inrush current

kva 2,300-volt units with the shortest possible leads through a breaker resulted in a first crest 133 times normal crest current. Similar tests paralleling two 360-kva 2,300-volt units resulted in 35 times normal. These values were closely predicted by calculations.

THREE-PHASE STAR-UNGROUNDED AND DELTA-CONNECTED CAPACITORS

An examination of Figure 7a shows the relation of transient currents in three-phase banks to those in a line-to-neutral circuit considered up to this time.

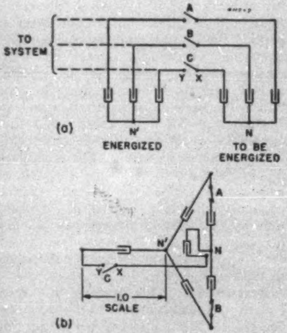

Figure 7. Paralleling ungrounded star-connected capacitors

When pole A is closed no current flows since the right-hand bank is ungrounded.

The voltage across pole B is now 1.73 times normal line-to-neutral voltage. The circuit to be completed when pole B closes has an impedance 2.0 times that of a single line-to-neutral phase. The maximum transient current is, therefore, $1.73/2=0.866$ times that calculated from a line-to-neutral circuit.

The voltage across pole C is now 1.5 times normal line-to-neutral voltage as shown in Figure 7b. The circuit that will be completed when it is closed has an impedance 1.5 times that of one phase. This circuit can be traced from X to N, through phases A and B to N', thence to Y. The maximum transient current is hence $1.5/1.5=1.0$ times that computed using the line-to-neutral circuit alone.

It is concluded that the maximum transient current when paralleling star-ungrounded banks occurs when the third pole closes and is the same as for a star-grounded bank,* and hence can be calculated using one reference phase only.

For delta capacitors of the same kilovolt-amperes the impedances as viewed from the breaker are the same and hence the currents are the same as for star-ungrounded banks.

DOUBLE-MAGNITUDE INRUSH CURRENTS

Since the 60-cycle current passes through zero at crest voltage a capacitor is left fully charged when disconnected from the system. Consequently, if not discharged prior to re-energizing, the switch may close on double normal voltage and result in a transient current twice that obtainable when energizing a fully discharged capacitor.

When three-phase ungrounded capacitors, either star or delta, are disconnected from the system one line-to-line phase is left charged at 237 per cent voltage, as will be shown in the next section. An out-of-phase reclosure can result in an inrush $(1.73+2.37)/2=2.05$ times the basic inrush given by Figure 6 and equation 2. Also, when a three-phase ungrounded capacitor is disconnected one phase is left charged at a voltage 2.05 above the average of the other two. On reclosure without discharging, the third pole can experience an inrush of $(2.05+1.50)/1.50=2.36$ times the basic inrush. These are based on interruptions at normal current zero such as were obtained throughout this series of tests.

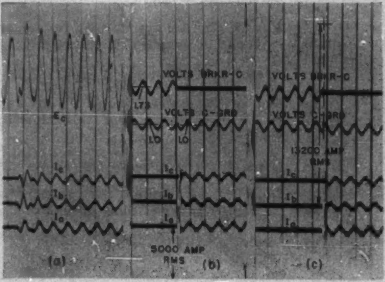

Figure 8. Inrush-current oscillograms

(a). Energizing 13,500-kva bank at end of long transmission line

(b). Energizing discharged 2,700-kva capacitor in parallel with 10,800-kva already energized

(c). Energizing charged 2,700-kva capacitor in parallel with 10,800-kva already energized

TRANSIENT-CURRENT TESTS

With this preamble, the test results will now be considered to see how well they conform with expectations. The maximum transients were obtained with four banks already energized and the fifth switched on. As the capacitors were not de-energized between tests inrush currents up to 2.36 times the basic value were possible. The capacitors were switched on-off-on-off, and so on, while the films were being run.

With four capacitors energized and a fifth one switched on, the reactances were:

$X_L=0.00031$ per unit $=0.031$ per cent
$X_C=1.25$ per unit $=125$ per cent
$X_L(X_C/100)=0.039$ per cent
$X_L(100/X_C)=0.025$ per cent

From Figure 6 the maximum transient current when energizing the discharged capacitor is 51 times normal. Upon energizing the capacitor left charged from a previous opening the inrush would be 2.36 times 51 or 121 times normal. The maximum expected current is therefore 121×148 amperes or 18,000 amperes rms. The expected frequency, from Figure 6, is 3,800 cycles.

The maximum current obtained was 13,200 amperes rms. It occurred at the closure of a second pole for which condition the theoretical maximum was $2.05\times51\times148=15,000$ amperes. The corresponding frequency as closely as could be measured was 3,000 to 5,000 cycles. Figure 8 shows this particular film. The traces, distinct only on the original, have been strengthened.

The 300-ampere refills used in the type *BA*-400 fuses would require 13,000 amperes for 1.0 cycle to be damaged. The transient current decays to a negligible value in 0.25 cycle.

RESTRIKING

If, upon closing, the breaker contacts bounced open and the current remained zero for a half cycle, after charging the capacitor up to full voltage or higher the next closure would result in a very large current transient. However, no breaks in any of the current waves were noted after the initial closure of breaker contacts. Hence, this phenomenon did not occur. Double-magnitude transients were obtained, however, by energizing charged capacitors as outlined previously.

Similarly, if while opening the arc had restruck, inrush transients up to 2.37 times the basic value would be obtained just as outlined under "Double-Magnitude Inrush Currents." However, no restriking of any kind was observed in spite of high recovery voltages to be described later.

DECREMENT OF TRANSIENT CURRENT

The rate of decay of the transient current is of some importance, particularly

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*This statement applies to energizing discharged banks. Later paragraphs cover charged banks.

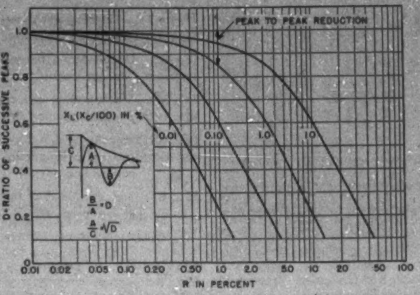


Figure 9. Decrement of capacitor inrush currents

where resistance is used between capacitors to limit the inrush current. The classical expression for the ratio of successive peaks is as follows. Refer to Figure 4c.

$$D=\frac{B}{A}=-0.5\pi R\sqrt{C/L} \qquad (7)$$

But this may be expressed in the per unit terms used heretofore.

$$D=-0.5\pi R/\sqrt{X_L X_C} \qquad (8)$$

The decrement D is plotted in Figure 9. Due to decrement the maximum inrush peak is reduced to the $\sqrt{D}$ times the value with no decrement. However, if the peak-to-peak reduction is not over 20 or 30 per cent the reduction of the first peak is very closely half of the peak-to-peak reduction. In the Newport News tests the ratio of successive peaks is about 0.8. Thus the maximum peak is only ten per cent less than with no decrement. This illustrates the small approximation involved when decrement is neglected in calculating the first peak, unless, of course, resistance is purposely inserted.

Voltages When Switching off Capacitors

Since the current always opened at normal current zero in these tests, the voltages resulting can be readily calculated. The voltages to ground, recovery voltages across circuit breakers, and the line-to-line and line-to-neutral voltages across capacitors following opening, are all of importance. They can be obtained from Figure 10 by noting the maximum values between various traces. The solid lines are the bus voltages on one side of the breaker, and the capacitor-terminal voltages are initially along these same traces.

The dotted lines show the capacitor-terminal voltages as they depart from the bus voltages in a normal opening. After current A is interrupted the corresponding capacitor terminal remains a fixed voltage above the average of B and C. The B and C currents are interrupted 90 degrees after A current. From then on all three terminal voltages are fixed.

This type of opening was observed in all cases except where poles were purposely by-passed. The resulting voltages, given in Table III A, are well within design expectations and the tests agree with them as closely as the films can be read. Typical oscillograms are given in Figure 11.

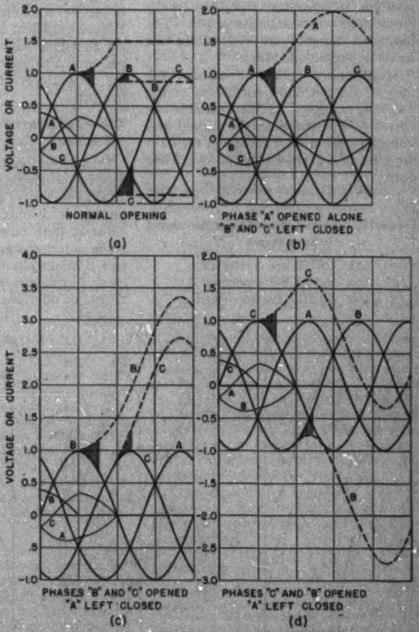
Special Controlled-Opening Tests

For the purpose of studying other applications in which the several poles might be opened an appreciable time apart, special tests were made under controlled-opening conditions.

In one set of tests one pole was permanently opened and one pole permanently by-passed. All the switching was done on the third pole. As shown in Table IIIB, derived from Figure 10 and fully confirmed by the tests, the voltages to ground can be much greater in this case than with normal opening. Also twice normal line-to-line voltage, or 346 per cent of normal line-to-neutral voltage is obtained across the open breaker pole. This is also shown in Figure 11.

In another series of tests one pole was by-passed, the other two poles operating normally. In this case the voltages depended on which of the two free phases opened first. The various voltages are listed in Table III C and all have been confirmed from various films similar to those of Figure 11. The highest capacitor-terminal voltage to ground is 337 per cent, but the highest voltage across a star capacitor leg is 137 per cent which is well within its short-time strength.

The voltages across capacitor legs are independent of the manner in which the opening is controlled so long as current is interrupted at normal current zero. The first leg to open is always left with 100 per cent voltage and the next two with 37 per



NORMAL OPENING
(a)

PHASE "A" OPENED ALONE "B" AND "C" LEFT CLOSED
(b)

PHASES "B" AND "C" OPENED "A" LEFT CLOSED
(c)

PHASES "C" AND "B" OPENED "A" LEFT CLOSED
(d)

Figure 10. Bus and capacitor terminal voltages to ground

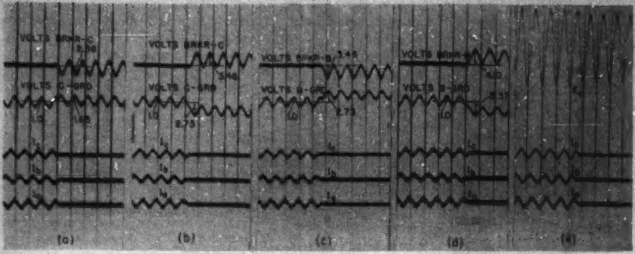


Figure 11. Oscillograms showing voltages during switching

(a), (c). Disconnecting capacitor with *C* phase opening first

(b), (d). Disconnecting capacitors with *B* phase opening first

(e). Disconnecting 13,500-kva capacitor from end of long transmission line

In a, b, c, and d, pole *A* of the breaker was by-passed

cent and 137 per cent voltage (87 per cent ± 50 per cent). The highest voltage across a breaker pole is 410 per cent or 2.36 times line-to-line voltage. In some cases this increase from 250 per cent with normal switching may be important.

WAVE SHAPE

The bus voltage and capacitor-current wave shapes are shown in Figure 12 for one, three, and five 2,700-kva units connected. It will be noted that harmonics are somewhat more prominent with three units in service than with either one unit or five units. This phenomenon has been observed previously,[1] namely, that harmonics tend to increase at first with addition of capacitors and then to decrease after a maximum is reached. The harmonic currents are well within the limits of proper operation.

Measurements taken throughout the day of the test show harmonics in the following ranges. As the readings occurred at different times the current and voltage ranges do not agree and the data are presented to show order of magnitude only.

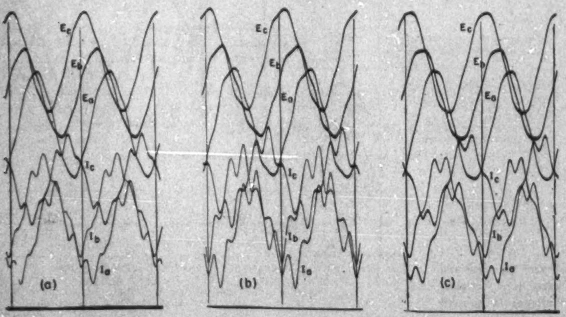


Figure 12. Bus-voltage and capacitor-current wave shapes

(a). 2,700 kva connected
(b). 8,100 kva connected
(c). 13,500 kva connected

Harmonic	Voltage, Per Cent	Current, Per Cent
3d, 180 cycles	0.3	2
5th, 300 cycles	3–7	23.5–36
7th, 420 cycles	0.25–1.6	6.3–13
11th, 660 cycles	Approximate 0.1	Approximate 1
13th, 780 cycles	Approximate 0.1	Approximate 1

The maximum fifth harmonic current of 36 per cent corresponds to 2.6 per cent kilovolt-amperes in the capacitor ($0.36^2 \times 1/5 = 0.026$) whereas industry standards require provision for up to 35 per cent kilovolt-amperes to take care of overvoltage and harmonics.

The current and voltage waves of Figure 12c, for the complete 13,500-kva bank in service, were analyzed by the Fischer-Hinnen graphical method and showed 3.9 per cent fifth harmonic voltage and 18.6 per cent fifth harmonic current. These are very closely in the expected one-to-five ratio.

TIF—TELEPHONE INTERFERENCE FACTOR

Current and voltage telephone interference factor readings are plotted in Figure 13 as a function of the kilovolt-amperes of capacitors connected. The maximum value of both current and voltage telephone interference factor is reached with two 2,700-kva units in service and decreases as more are added. The voltage telephone interference factor is about the same with five units in service as with none.

The voltage telephone interference factor of 8 to 16 may be compared with the National Electrical Manufacturers Association standard telephone interference factor limit of 50 for large synchronous machines.

CASCADING CAPACITORS

No ill effects were noted from the practice of utilizing lower-voltage units in series to make up high-voltage capacitors. A considerable saving in critical materials and cost is realized thereby since most of the major insulation to ground is provided in simple apparatus insulators instead of inside of each capacitor between the foil bundle and the case. The resulting saving in cost is indicated by the relative cost per kilovolt-amperes of 2,400-volt and 11,000-volt units in Table I.

Conclusions

1. **The voltage regulation when one 2,700-kva unit was switched on or off was one per cent. This compares with two per cent given by the rough rule that voltage incre-**

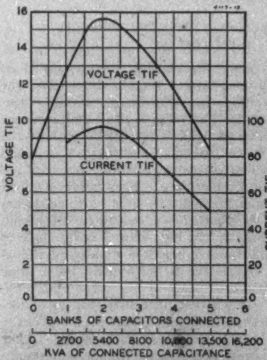


Figure 13. Current and voltage telephone interference factor as a function of connected capacitor kilovolt-amperes

Characteristics of Chlorinated Impregnants in D-C Paper Capacitors

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Synopsis: D-c capacitors of the impregnated-paper type play an important role in electronics equipment required by the armed services. In some of these applications good performance is required over very wide temperature ranges. A new chlorinated hydrocarbon composition which maintains reasonable capacitance constancy over a wide temperature range is described. The properties of this liquid are compared with conventional chlorinated impregnants. Since some alternating voltages usually accompany the direct voltages applied to capacitors, the a-c behavior over wide temperature and frequency ranges is described for capacitors impregnated with three different liquids. The effect of voltage and temperature on the resistance of capacitors is discussed. Considerable attention is devoted to the life behavior of capacitors impregnated with chlorinated liquids under d-c stresses at high temperatures. A life-testing procedure is described which has yielded very satisfactory results on capacitors of varying sizes and ratings. A means for prolonging the life of capacitors impregnated with chlorinated impregnants involving the addition of stabilizers is discussed. Data showing effect of a number of stabilizers are presented. Finally, some data are presented showing the effect of voltage on the life of d-c capacitors.

THE enormous demand by the armed services for electronics equipment has not only resulted in a corresponding increase in capacitor production but also has emphasized the need for good performance of capacitors in a wide variety of service conditions. Impregnated-paper capacitors are used in filter circuits, tuned audio-frequency circuits, and in circuits requiring blocking and by-pass elements. In some of these applications the capacitor is subjected to direct voltages accompanied by small but not always negligible alternating voltages. In other applications capacitors must withstand sustained direct voltages under high ambient temperatures. Hence, not only is the performance of the capacitor on high direct voltages important, but also its response to alternating voltages over wide temperature and frequency ranges. It is the purpose of this paper, therefore, to present data on both a-c and d-c properties which should be of aid in correctly applying capacitors containing chlorinated impregnants.

The a-c properties of an impregnated-paper capacitor are determined by both the paper dielectric and the impregnant. However, if the impregnant is a polar material, such as chlorinated diphenyl, it usually plays a dominant role in determining the a-c properties. Polar liquids depend on the rotation of molecules for part of the dielectric constant, and under certain conditions only part of the rated capacitance may be effective. At the higher frequencies, particularly if combined with low temperatures, the part of the dielectric constant contributed by molecular rotation may be lost through decreased response of the molecular dipoles to the applied field. A study of this problem led to the development of a new liquid composition which maintains its dielectric constant to considerably lower temperatures than is the case with the widely used chlorinated diphenyl impregnant.

The chlorinated aromatic hydrocarbons are known to have excellent electrical properties and stability toward oxidation. Despite this, however, a capacitor impregnated with them and subjected to a combination of sustained direct voltage and high temperatures has a definitely limited life. Capacitors of standard design built to operate at ambient temperatures of 40–50 degrees centigrade will withstand high stresses for long periods of time, but must be operated at reduced stresses when ambient temperatures of 75–85 degrees centigrade are encountered. Both high temperatures and sustained direct voltages are likely to be encountered in d-c filters and in blocking and by-pass applications. It therefore becomes important to study the life behavior of any new material in the finished capacitor. To this end a life-testing procedure has been developed which will be described fully. This test has proved valuable in studying means for prolonging the life of a capacitor. One method of producing longer life, consisting of the addition of

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The authors are indebted to C. F. Hill and J. G. Ford who with the co-operation of the Du Pont Company were responsible for the development of polychloroethylbenzene used in the new impregnating composition, and to R. Friedman, C. L. Cadwell, E. W. Lindsay, and M. Rutter for carrying out the tests on which this paper is based.

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ment is the ratio of capacitor kilovolt-amperes to short-circuit kilovolt-amperes. This does not alter the capacitor kilovolt-amperes required to supply the needed kilovars but simply affects the voltage increment obtained per step.

2. Inrush currents can be computed on a single-phase line-to-neutral base. If the maximum inrush when energizing one star leg, previously discharged, is termed the basic inrush, other values may be expressed as multiples thereof. A fully charged capacitor has a maximum inrush twice the basic value. Greater than normal charge is left on star-ungrounded or delta capacitors when disconnected and if they are energized without being first discharged, this may result in 2.36 times the basic inrush. Inrush currents of 50 to 200 times normal represent the order of magnitude encountered in paralleling capacitors of a bank.

3. There were no current openings caused by contact bouncing on breaker closure and no restrikings while opening and hence maximum inrush currents were not increased by such phenomena.

4. When star-ungrounded capacitors are disconnected by well adjusted oil circuit breakers, two of the currents stop 90 degrees after the first. One hundred per cent of line-to-neutral voltage is left on the first leg of the capacitor and 37 per cent and 137 per cent voltage on the other two legs. The maximum line-to-line voltage left on the capacitor is 237 per cent. The maximum voltage to ground is 150 per cent and the maximum recovery voltage across a breaker pole is 250 per cent.

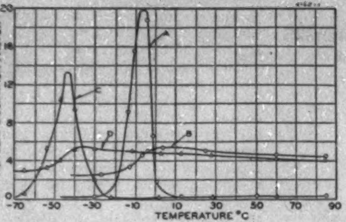
5. If the poles are opened individually with a cycle or more between, but the currents are extinguished at normal current zero, the capacitors are left charged to the same voltages as above but the maximum voltage to ground is 337 per cent and the maximum recovery voltage across an open breaker pole is 410 per cent or 2.36 times line-to-line voltage.

6. The general characteristic of a system is for harmonics to increase at first as capacitors are added and then for the wave shape to improve as further capacitors are added. In this installation the hump of the curve was reached with two of the five units in service the telephone interference factor being reduced as further units were added. For all conditions the harmonics were well within accepted industry levels.

7. The capacitor is considered a highly satisfactory solution to the problem of supplying the needed kilovars under the conditions involved.

Reference

1. SYSTEM LOWER-HARMONIC VOLTAGES—METHODS OF CALCULATION AND CONTROL BY CAPACITORS, W. C. Foester, E. L. Harder. AIEE TRANSACTIONS, volume 60, 1941, pages 1060–6.



A—Dissipation factor–conventional Inerteen
B—Dielectric constant–conventional Inerteen
C—Dissipation factor–special Inerteen
D—Dielectric constant–special Inerteen

Figure 1. Dissipation-factor and dielectric-constant behavior as functions of temperature for two chlorinated impregnants—measured at 60 cycles per second

stabilizers to the impregnant, will be described.

Physical and Electrical Properties of Several Chlorinated Impregnants

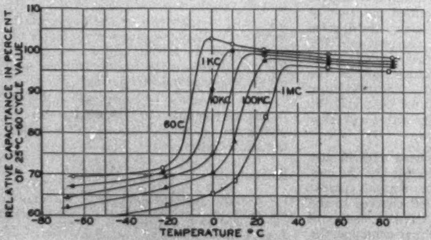
Chlorinated aromatic hydrocarbons have found considerable use as impregnants in the electrical industry because of their good oxidation stability, noninflammability, and desirable electrical properties. These materials have largely replaced mineral oil and waxes as capacitor impregnants because of their higher dielectric constants and good stability under high stresses at moderate temperatures. These materials have been the subject of a number of investigations, and the results are reported in the literature.[1-4] While several types of chlorinated hydrocarbons have been used, the type which has come into the most extensive use in capacitors is a chlorinated diphenyl averaging about five chlorine atoms per molecule. Some of the physical and electrical properties of this liquid, which will be referred to hereafter as "conventional Inerteen" are given in the first part of Table I and by curves *A* and *B* of Figure 1. The electrical properties apply only after the liquid has been carefully purified.

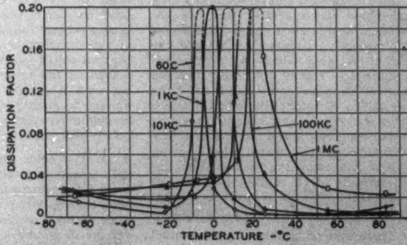
As Table I shows, conventional Inerteen is a viscous liquid with a pour point of +10 degrees centigrade. Curve *B* of Figure 1 shows that the dielectric constant at 60 cycles begins to fall with decreasing temperature at about zero degrees centigrade. At higher frequencies the dielectric constant starts to fall at even higher temperatures as will become apparent later. For most normal applications, and particularly in d-c filter circuits, this results in no serious difficulties. However, for certain uses, such as in tuned circuits and by-pass applications, it is desirable to have a liquid which does not suffer a marked reduction in dielectric constant until considerably lower temperatures are reached. For such applications a new liquid composition was developed consisting of chlorinated diphenyl blended with a substantial portion of polychloroethylbenzene (nuclearly chlorinated). The properties of this liquid, which will be referred to hereafter as "special Inerteen," are given in the last column of Table I and by curves *C* and *D* of Figure 1.

Comparison of the properties of special Inerteen with those of conventional Inerteen shows that the former has considerably lower viscosity and pour point than the latter. This makes possible unrestricted rotation of dipoles in special Inerteen at 60 cycles down to about −40 degrees centigrade as curve *D* of Figure 1 shows. Thus, for any given frequency a capacitor impregnated with special Inerteen will maintain its capacitance to considerably lower temperatures than is the case for conventional Inerteen. Comparison of curves *A* and *C* of Figure 1 shows that special Inerteen properly processed has a dissipation factor at 60 cycles which is as low as that for conventional Inerteen in the 20–85 degrees centigrade temperature region. In this temperature range, however, special Inerteen has a slightly lower dielectric constant than conventional Inerteen as indicated by curves *B* and *D* of Figure 1. This results in less than five per cent reduction in capacitance per unit of volume as compared with conventional Inerteen. Figure 1 also shows the usual dissipation-factor peaks which always appear in polar substances in the middle of the dielectric-constant dispersion region.

Figure 2 (left). Capacitance–temperature behavior at various frequencies of capacitor impregnated with conventional Inerteen

Figure 3 (right). Dissipation factor–temperature behavior at various frequencies of capacitor impregnated with conventional Inerteen





Electrical Properties of Capacitor Impregnated With Chlorinated Impregnants

Effect of Temperature and Frequency on Capacitance and Dissipation Factor*

In the foregoing, the electrical properties of the liquids alone have been treated. Of even more practical importance is the behavior of these liquids in combination with paper. The dissipation factor and capacitance behavior over wide frequency and temperature ranges should be an invaluable aid in properly applying capacitors. As far as the writers know, such data have not previously been published except for the case of a mineral-oil-impregnated capacitor.[5] For this reason dissipation-factor and dielectric-constant data over the frequency range of 60 cycles to 1,000 kilocycles and the temperature range from −70 to +85 degrees centigrade were obtained on capacitors impregnated with the two synthetic liquids just described. For purposes of comparison with a nonpolar impregnant, a similar study was also made on a mineral-oil-impregnated ca-

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* Dissipation factor is defined as the tangent of the loss angle or tan δ, whereas power factor is the sine of the loss angle or sin δ. For values of either less than 0.1 the two may be considered numerically equal for all practical purposes.

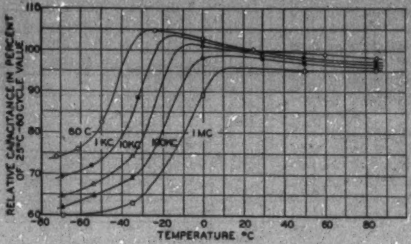


Figure 4. Capacitance–temperature behavior at various frequencies of capacitor impregnated with special Inerteen

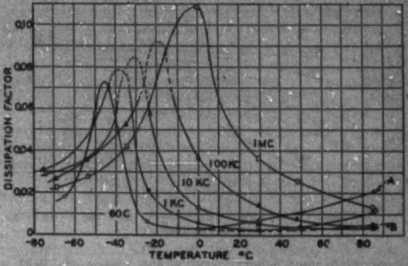


Figure 5. Dissipation factor–temperature behavior at various frequencies of capacitor impregnated with special Inerteen

pacitor. The sample capacitors consisted of three layers of 0.4-mil kraft paper placed between aluminum-foil electrodes. The paper was carefully dried and thoroughly impregnated.

Results of capacitance and dissipation-factor measurements for a capacitor impregnated with conventional Inerteen are given in Figures 2 and 3. The capacitance values are plotted in Figure 2 in per cent of the 60-cycle capacitance at 25 degrees centigrade for five different frequencies. The accompanying dissipation-factor data are presented in Figure 3. A similar set of data for a capacitor impregnated with special Inerteen is given in Figures 4 and 5. A third set of data for a mineral-oil-impregnated capacitor is shown in Figures 6 and 7. The mineral oil is of the highly refined and stabilized type, having a Saybolt viscosity at 100 degrees Fahrenheit of 72 seconds and an American Society for Testing Materials pour point lower than −40 degrees centigrade.

It is interesting to contrast the capacitance behavior of the capacitor impregnated with the nonpolar mineral oil with those impregnated with the polar liquids. If we refer to Figure 6, it is evident that in the case of the mineral-oil-impregnated capacitor, the capacitance decreases gradually with decrease in temperature at all of the frequencies. This behavior is determined largely by the paper since the oil alone would be expected to show a slight increase in capacitance with decrease in temperature because of corresponding increase in density. It should be noted also that the rate of decrease of capacitance with decrease in temperature becomes greater with increasing frequency. Examination of Figure 6 shows that at −40 degrees centigrade, for example, the capacitance at 60 cycles is more than 99 per cent of 25 degrees centigrade value. At one megacycle and −40 degrees centigrade, however, the capacitance is only 84.5 per cent of the 25 degrees centigrade–60 cycle value. This may be taken as the most constant capacitance characteristics obtainable with an impregnated paper capacitor. Other mineral oils with higher liquid-to-solid transition temperatures result in decidedly greater decrease in capacitance at low temperatures.

In the case of the capacitors impregnated with the chlorinated polar liquids, the capacitance behavior is somewhat different as Figures 2 and 4 show. In the higher-temperature region there is at first a slight increase in capacitance with decrease in temperature, which is followed by a sharp decrease in capacitance with further decrease in temperature. It is also apparent that the region of sharp decrease in capacitance, known as the dispersion region, is shifted to successively higher temperatures with increasing frequency. These capacitance changes are determined largely by the polar liquids and the paper plays only a secondary role.

Comparison of the capacitors impregnated with the two polar liquids brings out some interesting facts. Figure 2 shows that for conventional-Inerteen-impregnated capacitors, the capacitance at −40 degrees centigrade and 60 cycles falls to approximately 70 per cent of the 25 degrees centigrade value. At one megacycle, the capacitance is reduced to 60 per cent of the 25-degree-centigrade 60-cycle value. For special-Inerteen-impregnated capacitors, on the other hand, the capacitance at −40 degrees centigrade and 60 cycles is still 95 per cent of the 25 degrees centigrade value. At one megacycle and −40 degrees centigrade, however, the capacitance of the special-Inerteen-impregnated capacitor is only slightly higher than that for conventional-Inerteen-impregnated capacitors. Thus, it is evident that a special-Inerteen-impregnated capacitor may be used in those applications where reasonable capacitance constancy is desired over a wide temperature range, particularly at the lower frequencies. In this respect, special Inerteen is intermediate between mineral oil and conventional Inerteen but with the distinct advantage over mineral oil of higher dielectric constant.

If we pass now to the dissipation-factor behavior, Figure 7 shows that the mineral-oil-impregnated capacitor has the broad dissipation-factor peaks characteristic of cellulose. It is well known that cellulose exhibits polar properties. Some interesting data on cellulose and cellulose derivatives are reported by Stoops.[8] Since the mineral oil investigated in this case is nonpolar, that is, it has no permanent dipole moment, the polar characteristics of the impregnated capacitor are entirely determined by the paper. It should be noted that the peaks in Figure 7 shift to successively higher temperatures with increasing frequency. Furthermore, the peaks are located in the middle of the capacitance dispersion regions given in Figure 6, as is required by dipole theory.

The characteristics of the capacitors impregnated with the polar liquids are quite different from those of the mineral-oil-impregnated capacitor. Figure 3 gives the data for conventional Inerteen. It is seen that the experimenal data were not sufficient to outline the peaks in detail, and hence they were drawn in an approximate manner as is indicated by the dashed lines. However, it will be noted that the peaks are much higher and much sharper than for the mineral-oil-impregnated capacitor. In this case the polar behavior of the liquid and cellulose are superimposed, and that of the liquid is pronounced enough to mask that of the cellulose. The dissipation-factor curves for special Inerteen, shown in Figure 5, are somewhat intermediate between the other two. The peaks are broader for special Inerteen than for conventional Inerteen because of a greater distribution of relaxation times brought about by the greater molecular complexity of the former.

In addition to the frequency and temperature effects on the dissipation factor of a capacitor impregnated with chlorinated impregnants, there is also a voltage effect at the lower frequencies and the higher temperatures. This is indicated by the two branches of the 60-cycle curve marked *A* and *B* of Figure 5. Branch *A* was determined at voltages of the order of 20 volts per mil, whereas branch *B* was determined at 200 volts per mil. The effect of voltage on dissipation factor is

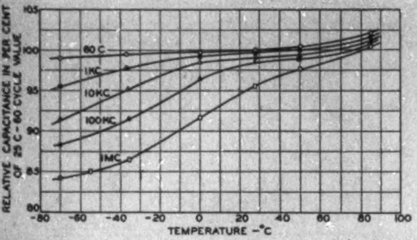


Figure 6 (left). Capacitance–temperature behavior at various frequencies of capacitor impregnated with mineral oil

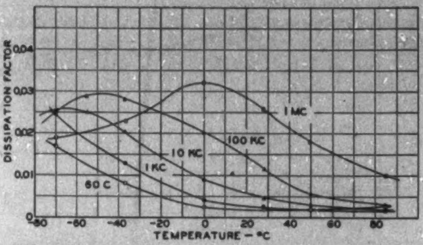


Figure 7 (right). Dissipation factor–temperature behavior at various frequencies of capacitor impregnated with mineral oil

illustrated in more detail for both of the chlorinated impregnants in Figures 8 and 9. This behavior is caused by the restricted motion of ions in the pores of the paper. The theory is very lucidly described by Garton[7] and will not be repeated here. This phenomenon also has been observed in oil-impregnated paper, but the effect is not so pronounced. Attention should be called to the fact that all of the data of Figures 3, 5, and 7 were determined at low-voltage stresses except at 60 cycles and then only in the higher-temperature region. The voltage effect appears to be negligibly small above 10 kilocycles even at 85 degrees centigrade.

If we consider the dissipation-factor data from the practical point of view, it is evident that high dissipation factors are encountered at the higher frequencies, regardless of the impregnant that is used. At one megacycle, even the mineral-oil-impregnated capacitor can have a dissipation factor greater than 0.03. In the case of capacitors with the chlorinated impregnants, considerably higher values are possible. Of course, if voltage of a given frequency is applied at a temperature where the dissipation factor is near a maximum, heat is generated which raises the temperature of the capacitor, thus tending to reduce the losses. However, at high ambient temperatures even these reduced a-c losses when added to the d-c losses may bring about thermal instability.

Therefore, the designer of electronic equipment should consider the reaction of the circuit to capacitance and dissipation-factor changes with temperature and frequency. Furthermore, he should not overlook the possibility of thermal instability arising in the capacitor through superposition of a-c losses on the d-c losses, especially if the ambient temperature is high. He should remember also that the average d-c impregnated-paper capacitor is not designed to carry large currents in attempting to apply it in circuits involving alternating voltages. In the higher -frequency by-pass applications, for example, the alternating currents can become quite large even though the alternating voltage impressed on the capacitor be small. Such currents will add to the total heat generated through I^2R losses in the leads and even the metal foils. In many cases of this kind specially designed high-current capacitors should be used.

Effect of Voltage and Temperature on Resistance

The strictly d-c losses in a capacitor are determined by its resistance. Not only is the magnitude of the resistance important, but also its change with temperature and voltage. It has been the practice in the capacitor art to report resistance values as the product of the resistance in megohms and the capacitance in microfarads or *RC*. This not only gives the internal time constant of the capacitor, but also places capacitors of the same voltage rating but of different capacitances on the same basis. It is easy to see that the latter is true, when it is realized that, for the same thickness of dielectric, the resistance varies inversely as the area of the dielectric, and the capacitance varies directly as the area. Since resistance varies with time of application of voltage, it is customary to read the resistance a fixed time after applying the voltage. In this work readings were taken two minutes after application of voltage. The previous history of voltage application to the capacitor is also important. In order to avoid polarization effects,[8] it is advisable to short-circuit the terminals for a time before making the test.

Figure 10 gives some typical data for one-microfarad units impregnated with the chlorinated impregnants. Several of the curves are for stabilized liquids. The function of stabilizers will be discussed more fully in the next section. It should be noted that the resistance decreases not only with temperature but also with voltage. At 25 degrees centigrade there is little difference between the two liquids, even with stabilizers added, and the values usually fall within the shaded area between the curves. Such variations as do exist are due more to minor variations in processing than to differences in the impregnants.

At the higher temperatures conventional Inerteen usually results in a capacitor with slightly higher resistance than is the case with special Inerteen. Some stabilizers, such as the one referred to in Figure 10, reduce the resistance somewhat though not seriously. In general, the resistance is a sensitive function of the processing, and careful processing is extremely important in maintaining high resistance. At normal ambient temperatures and voltage stresses the resistance of capacitors made with either liquid is high enough to be of little concern to the user. However, the fact that the resistance decreases with both temperature and voltage is an important consideration in establishing a life-testing procedure which will now be discussed.

Life Behavior Under D-C Stress

Description of Testing Procedure

It is most important that a capacitor have a long life under the temperature and voltage-stress conditions for which it was designed to operate. Unfortunately, tests under such conditions would require so long a time that it would be almost impossible to evaluate advancements in the art. Therefore, it is necessary to resort to an accelerated test in which the voltage stress or temperature or both exceed normal values. Extreme care, however, must be exercised in choosing the conditions in order to avoid introducing some factor which does not obtain in service. For example, if the voltage stress is too high, internal ionization may take place, or, if the temperature is too high, a different type of chemical action may be induced. Also, excessive temperatures or voltages may result in internal thermal instability, a condition which would be predicted by the rapid decrease of resistance with increasing temperature and voltage as indicated in Figure 10.

The actual test conditions used to obtain the results of this investigation were chosen after considerable exploratory work at voltage stresses in the range from 500 to 1,500 volts per mil and temperatures varying from 70 to 100 degrees centigrade. It was found that the thermal

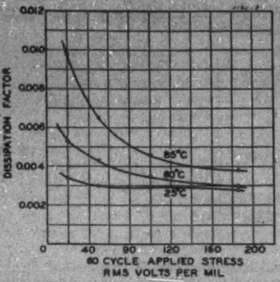


Figure 8. Dissipation factor–voltage characteristics of capacitors impregnated with conventional Inerteen

rise in even the small commercial units has an effect on the life when tested at stresses and temperatures which will produce failures in less than one month. Under such conditions even the capacitor case may reach temperatures appreciably above ambient. Low resistance and large size of test unit, of course, will accentuate both the internal rise and the case rise above ambient temperature. Such a test, therefore, would not be indicative of the true life, since the result would be determined by these factors rather than the inherent chemical and electrical stability of the dielectric.

The insulation rise above test temperature reduces the resistance still further, thereby tending to make the rise greater until a critical condition is reached, after which the specimen will fail in a few hours as a result of cumulative heating. For example, a one-microfarad 500-volt capacitor tested at twice rated voltage in quiescent air at 75 degrees centigrade became thermally unstable and drew 35 watts before the actual breakdown occurred after less than 24 hours on test. The dark brown color of the paper dielectric indicated that temperatures in excess of 150 degrees centigrade had been reached.

The temperature-rise variable has been largely eliminated in all of the experimental capacitors used in this work by stressing only a small part of the available volume. Commercial 600-volt two-microfarad units, were used in which regular production was duplicated, except that the length of one foil was reduced to a few feet nearest the outside of the case. The dielectric consisted of three sheets of 0.4-mil kraft paper which upon impregnation with the chlorinated impregnants yielded a capacitance of approximately 0.25 microfarad. Such capacitors do not show a rise in case temperature over an ambient temperature of 85 degrees centigrade even when stressed at 1,200 volts per mil. These experimental units are useful particularly in studying the chemical and electrical stability of the dielectric and in investigating the effect of addition agents to the impregnant. Data on these small units can be applied in arriving at the life of much larger capacitors in which the actual hot-spot temperature is known or can be computed.

There is a necessity, however, for some tests which will integrate all variables and directly compare all sizes of capacitors. Although the internal rise varies somewhat with the resistance and size of the unit, a considerable degree of success at standardization of tests has been achieved by keeping the case temperature within a few degrees of ambient temperature by means of air circulation. It was found that the case-temperature rise of most units could be held close to the desired ambient temperature by forcing air around them at velocities as low as 100 feet per minute. The actual air velocity required is not only a function of the size and the resistance of the unit, but also of the time in which it is desired to produce failures. There is, however, an obvious limit to which the testing time can be shortened, particularly on large units, because of the internal temperature gradient. An air velocity of approximately 250 feet per minute was finally used in this work, because it produced results in the desired time without excessive temperature rise. Attention was given to the uniform spacing of the units and even to the paint finish which may affect the outcome of a test near the point of thermal instability.

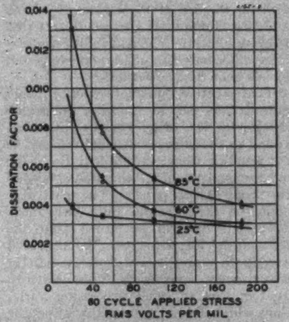


Figure 9. Dissipation factor–voltage characteristics of capacitors impregnated with special Inerteen

⊙—Unstabilized Inerteen
△—Inerteen plus 0.5 per cent stabilizer 1

In tests of this nature, voltage usually is applied to a considerable number of units in parallel. Unless precautions are taken, the surge resulting from the failure of one unit can cause damage to the remaining units under test. This problem was solved by placing a resistor and a 0.125-ampere fuse wire in the ground return lead of each unit. The length of the fuse wire was such that an arc could not be maintained across its terminals. The use of a series resistor not only reduced the surge on the remaining units, but also provided a means for measuring the leakage currents from time to time. A vacuum-tube voltmeter connected across the resistor served as a microammeter. In the case of the larger units, a milliammeter could be shunted across the resistor and the current read directly, because the resistance of the milliammeter was small compared with that of the resistor. This made it possible to read the leakage currents without even momentary interruption of the test.

Life-Test Results and Discussion

Beside determining the life behavior of conventional-Inerteen- and special-Inerteen-impregnated capacitors, one of the principal purposes of this investigation is to ascertain how the life of these capacitors can be prolonged. McLean and coworkers[8] made an extensive study of the behavior of capacitors impregnated with chlorinated naphthalene under sustained direct voltage. They present some data which show that a chlorinated-diphenyl-impregnated capacitor possesses a higher order of stability than one impregnated with chlorinated naphthalene. They also present evidence that the mechanism of failure is essentially the

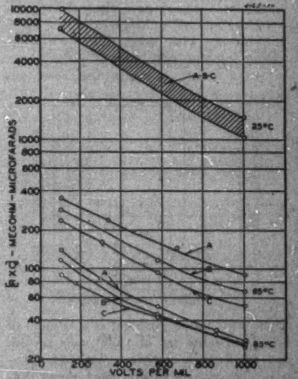


Figure 10. Resistance characteristics of one-microfarad 5,000-volt d-c capacitors—two-minute values

A—Conventional Inerteen
B—Conventional Inerteen plus 0.1 per cent stabilizer 1
C—Special Inerteen plus 0.5 per cent stabilizer 1

Table I. Some Physical and Electrical Properties of Chlorinated Capacitor Impregnants*

	Conventional Inerteen	Special Inerteen
Density 25/25 C	1.542	1.525
Refractive index—25 C	1.638	1.614
Pour point—ASTM—degrees centigrade	+10	−28
Flash point—open cup, degrees centigrade	None	180
Fire point—open cup, degrees centigrade	None	None
Viscosity—Saybolt seconds at 100 F	2,800	125
Viscosity—Saybolt seconds at 210 F	46	35
Dissipation factor at 25 C—per cent**	<0.1	<0.1
Dissipation factor at 85 C—per cent**	0.1	0.1
Dielectric constant at 25 C**	5.0	4.5
Dielectric constant at 85 C**	4.5	4.0

* All values of the various properties listed are approximate only, and they may vary slightly from batch to batch.

** All dissipation-factor and dielectric-constant values were determined at a frequency of 60 cycles per second after the liquids had been subjected to a thorough purification treatment.

same in both cases. It starts with the splitting off from the chlorinated-hydrocarbon molecule of hydrogen chloride which attacks the aluminum foil to form aluminum chloride. Aluminum chloride in turn catalyzes further decomposition of the impregnant and perhaps even the paper.

The application of d-c stress plays a dominating role, as is indicated by the finding that the life under a-c stress is considerably longer than with d-c stress of comparable magnitude, other conditions being the same. Some electrolysis phenomena, which would be expected to be more severe with direct than with alternating current, undoubtedly take place.

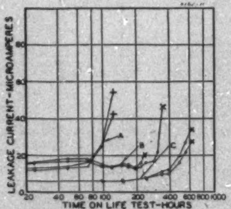
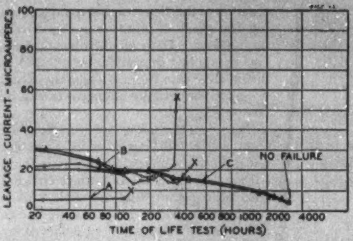


Figure 11. Leakage current as a function of time on d-c life test at 1,000 volts per mil and 85 degrees centigrade for 0.25-microfarad capacitor made with three sheets 0.4-mil kraft paper and impregnated with conventional Inerteen

A—Conventional Inerteen, unstabilized
B—Conventional Inerteen plus 0.1 per cent stabilizer 1
C—Conventional Inerteen plus 0.1 per cent stabilizer 5
X—Denotes failure



A—Special Inerteen, unstabilized
B—Special Inerteen plus 0.1 per cent stabilizer 1
C—Special Inerteen plus 0.5 per cent stabilizer 1
X—Denotes failure

Figure 12. Leakage current as a function of time on d-c life test at 1,000 volts per mil and 85 degrees centigrade for 0.25 microfarad capacitor made with three sheets 0.4-mil kraft paper and impregnated with special Inerteen

On the assumption that hydrogen chloride is the chief product of this electrolytic action, an attempt was made to find chemical compounds which when added to the chlorinated impregnants would prolong the life of the capacitor. Such addition compounds are usually referred to as stabilizers. The possibility of stabilizing chlorinated impregnants is suggested by McLean and coworkers.[9]

That effective stabilizers for chlorinated impregnants can be found is illustrated in Table II. The various materials added to the impregnants are referred to by number since a detailed discussion of the chemistry of stabilization is beyond the scope of this paper. The tests were made on experimental units previously described. It will be noted that the *RC* values for capacitors impregnated with both conventional and special Inerteen vary somewhat. This is attributed in part to small processing variations which did not appreciably affect the life behavior. Some stabilizers reduce the *RC* values for both liquids while others result in increased *RC* values. Stabilizer 5, which produces a fivefold increase in life when 0.1 per cent is added to conventional Inerteen, is an example of the latter.

Table II shows also that, not only does special Inerteen have approximately the same life as conventional Inerteen, but also that stabilizers are equally effective in both liquids. The life usually can be increased by increasing the amount of stabilizer. For example, special Inerteen with 0.1 per cent by weight of stabilizer 1 has an average life of 384 hours under the conditions of test. Increasing the stabilizer content to 0.5 per cent results in a life in excess of 3,000 hours, despite the reduced *RC* value.

Additional data obtained on experimental capacitors showing the relative behavior of the two liquids are given in Figures 11 and 12. In these, the leakage currents under test conditions are plotted as a function of time. The degree to which results can be duplicated is indicated by the twin curves shown. In many cases, warning of impending failure was given by a rapid rise in leakage current, although this was not always the case. Comparison of Figures 11 and 12 shows that the two chlorinated impregnants have quite comparable behavior.

In the case of special Inerteen with 0.5 per cent of stabilizer 1, it is observed that the leakage current was rather high at the beginning of the test. As the test proceeded, however, there was a gradual decrease in current, and after 3,000 hours the leakage current approached that of lowest value observed for an unstabilized capacitor. A lesser amount of the same stabilizer in conventional Inerteen also shows some decrease in leakage current before the final rise preceding failure, as is indicated in Figure 11. However, not all stabilizers behave in this manner. If we refer again to Figure 11, it is observed that the leakage current in the case of curve *C* not only is low but remains practically unchanged until the final rise takes place. This class of stabilizer is preferable to one of which stabilizer 1 is an example.

Figure 13 gives some typical results obtained on commercial units impregnated with conventional Inerteen. Simi-

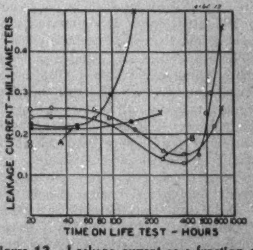


Figure 13. Leakage current as a function of time on d-c life test at 1,160 volts per mil and 85 degrees centigrade for 1.0-microfarad 5000-volt capacitors

A—Conventional Inerteen
B—Conventional Inerteen plus 0.1 per cent stabilizer 1
X—Denotes failure

lar results were obtained with special Inerteen. The tests were conducted according to the special procedure already described. The general behavior is the same as that observed in the smaller experimental units, as a comparison with Figure 11 will show. It should be noted, however, that, even though the stress is somewhat higher, a slightly longer life was observed for the larger units than for the small units of Figure 11. This is attributed to the use of a greater number of sheets of paper in the larger units. The greater the number of sheets of paper the less is the probability of superposition of conducting particles or other defects. This fact has been appreciated, and allowance is made for it in practical capacitor designs.

EFFECT OF VOLTAGE ON LIFE

So far, all of the life data presented on capacitors impregnated with the two chlorinated impregnants are for voltage stresses in the neighborhood of 1,000 volts per mil. For obvious practical reasons it is important to be able to predict the life at other voltage stresses. The following power law has been used for many years to express the life of impregnated paper as a function of voltage stress:

$$L_1 = L_2(E_2/E_1)^n$$

where L_1 is the life at the higher voltage stress E_1, L_2 is the life at the lower stress E_2, and n is a number reported by various investigators[9,10] to range between 4 and 8. Brotherton,[10] reporting on some recent work of this character carried out at the Bell Telephone Laboratories, states that n varies from 4 to 6 for capacitor impregnants in general use at the present time.

Work of this nature is very time-consuming, and many capacitors must be tested to destruction over both a range of voltage and a range of temperature. The data available at the present time, obtained at several voltages and 85 degrees centigrade on conventional Inerteen, both unstabilized and stabilized, are presented in Table III. The data are based on a large number of experimental capacitors constructed and processed in an identical manner. Those few capacitors which failed in a short time because of constructional and material defects were eliminated from consideration.

Examination of Table III reveals that, for conventional Inerteen under the conditions of test, n varies from a maximum of 5.1 to a minimum of 3.2 with an average value of 4.3. For the same impregnant with stabilizer 1 added, the corresponding values are somewhat higher However, before it can be concluded that stabilizers raise the value of n, many more tests must be made on other stabilizers. The average values of n found in this work lie within the range reported by Brotherton. Because of the similarity in chemical structure of two impregnants, it is to be expected that special Inerteen should show approximately the same value of n as for that conventional Inerteen.

Table II. D-C Life of Impregnated-Paper Capacitors at 85 Degrees Centigrade and 1,000 Volts Per Mil

Capacitors Consist of Three Sheets of 0.4-Mil Kraft Paper and Aluminum Foils

Impregnant	RC at 85 C Megohms-Microfarads	Average Life (Hours)
Conventional Inerteen	27–40	120
Same plus 0.1% stabilizer 1	21.5	336
Same plus 0.1% stabilizer 2	33	288
Same plus 0.1% stabilizer 3	33	360
Same plus 0.1% stabilizer 4	23	432
Same plus 0.1% stabilizer 5	50	600
Same plus 0.1% stabilizer 6	43	228
Special Inerteen	25–35	120
Same plus 0.1% stabilizer 1	20–27	384
Same plus 0.5% stabilizer 1	18–25	<3,000

Conclusions

The more important results of this investigation which deserve emphasis are the following:

1. The a-c characteristics of the capacitor are important to the proper performance of both the circuit and the capacitor in circuits involving alternating as well as direct voltages.

2. Paper capacitors impregnated with conventional chlorinated diphenyl show a considerable decrease in capacitance at low temperatures.

3. A recently developed chlorinated-liquid composition maintains reasonably constant capacitance to considerably lower temperatures than is the case with the chlorinated-diphenyl impregnant.

4. Accelerated life testing, properly carried out and interpreted, is an important tool in evaluating new materials and processes.

5. The life of a paper capacitor impregnated with chlorinated-hydrocarbon liquids when subjected to high d-c stresses decreases appreciably with increase in temperature.

6. The life of a chlorinated liquid impregnated capacitor at high d-c stress, and temperature can be increased markedly by the addition of stabilizers to the impregnant.

7. The increase in life of a capacitor containing a chlorinated impregnant is a function of the amount of stabilizer added. In some cases, approximately tenfold increase in life is obtained when the amount of stabilizer added is increased from 0.1 to 0.5 per cent.

8. The life of a chlorinated diphenyl impregnated capacitor under d-c stress at 85 degrees centigrade varies inversely as 4.5th power of the voltage applied.

Table III. D-C Life as a Function of Voltage Stress at 85 Degrees Centigrade on Experimental Capacitors Made With Three Sheets of 0.4-Mil Kraft Paper and Aluminum Foil—Capacitance 0.25 Microfarad

	Life—Hours		
	720 Volts Per Mil	1,000 Volts Per Mil	n
For Conventional Inerteen			
Maximum	648	144	5.1*
Minimum	408	120	3.2**
Average	535	130	4.3
For Conventional Inerteen Plus 0.1 Per Cent Stabilizer 1			
Maximum	1,920	384	6.3*
Minimum	1,296	240	3.7**
Average	1,584	322	4.8

* Based on maximum life at 720 volts per mil and minimum life at 1,000 volts per mil.

** Based on minimum life at 720 volts per mil and maximum life at 1,000 volts per mil.

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The Design of Low-Voltage Welding Power Distribution

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Synopsis: This paper is an effort to state clearly and to present a solution for the problems encountered in the design of one of the vital constituents of a-c resistance-welder power supply—the low-voltage feeder. The scope of the paper is much wider, however, since the determination of design loads from the probability standpoint for the purpose of determining maximum allowable impedance and expected temperature rise is a problem encountered in the design of all equipment common to a number of resistance-welding installations.

The topics considered in the paper are:

1. Definition of voltage regulation as it applies to resistance-welding equipment.
2. Establishment of a criterion of good voltage regulation as defined.
3. Application of the theory of probability to the determination of maximum tolerable impedance and minimum copper section consistent with safe operating temperature.
4. Calculation of reactance of the closely spaced rectangular bar bus-type feeder, which the authors believe is best suited for the job, and its nearest competitor for low reactance, the concentric tubular feeder.
5. Calculation of maximum current capacity in terms of allowable temperature rise in the aforementioned type of bus feeders.
6. Determination of force between conductors in flat bar bus under short-circuit conditions.
7. The complete design of a feeder starting with typical data.

FROM the standpoint of voltage regulation, as well as from that of the supply system, the a-c resistance welder is the black sheep of electric receiving apparatus. It combines in one type of load practically all of the undesirable features a load can have: high instantaneous demand, poor power factor, and low average power requirements. However, careful analysis of the problems involved can make it possible to arrive at designs of welding machines and associated equipment which will minimize the effects of these characteristics. It is one of these pieces of associated equipment, the welder supply feeder or bus, with which we are concerned.

Voltage Regulation

The ideal condition for a power supply is such that, when the welding circuit is energized with a given setting, the current is the same for each weld or the primary voltage of the loaded welding transformer is the same for each weld. For example, if there were only one machine connected to a particular feeder, the voltage drop on the feeder if within reasonable limits would be unimportant, because it always would be the same for a given current.

Unfortunately, from this point of view, usually several, and sometimes many, welders are supplied by a single feeder. Thus, if two or more welders requiring the same current are energized during the same period, the voltage drop will be two or more times that due to a single welder, and, if the drop with one welder is large, the increased drop will be so serious as to cause poor welds or no welds on the machines "hitting" together.

It would be possible of course to employ a device which would prevent more than one machine from welding at the same time. Unfortunately, the number of machines on some feeders is so great that the resulting delay would be serious under conditions of high production.

Although the ideal obviously would be a feeder on which the voltage drop would not exceed the permissible tolerance even when any probable number of overlaps (that is, machines welding at given time) might occur, this is practically impossible, particularly when the distribution voltage is low. Thus, it is of great importance to reduce the single machine drop to a minimum, so that the probable frequency of such multiple overlaps and the resulting voltage drop will be as small as possible. Since available voltage compensators[1] or current regulators[2] require a cycle or more to get under way and since the range of correction is limited, it is still desirable to reduce the feeder drop to a minimum. For a given feeder carrying a given current the numerical voltage drop is exactly equal to the feeder impedance voltage only when the load phase angle is equal to that of the feeder. In other cases the numerical drop is less than the feeder impedance voltage, although usually by a relatively small amount. If the feeder phase angle is very low as compared with the load phase angle, the numerical drop may be even less than the feeder resistance drop. These relationships are illustrated in Figure 1.

It becomes necessary, therefore, to establish a criterion of good voltage regulation and more important *to define the conditions under which the voltage regulation should be measured or calculated.*

There is so much looseness in the matter of definitions and terminology that it will pay to be more specific as to what we mean by "feeder regulation," particularly in its relation to the over-all voltage regulation. The ideal is obviously that the voltage at the primary terminals of the welding transformer should be constant. Actually, its variation is due to:

(*a*). The variation in the primary high voltage supplied to the power transformers of the plant in question.

(*b*). The variation in the voltage drop through these transformers and the intermediate-voltage feeders to the primary windings of the substation transformers.

(*c*). The variation in drop through these substation transformers.

(*d*). The variation in drop in the welding feeders now under consideration.

Although in a reasonably well-designed system *a*, *b*, and *c* are relatively small, they may be decidedly appreciable under maximum-load conditions. Our ultimate criterion must be the permissible variation in voltage at the primary winding of the welding transformers, or of that one which experiences the maximum variation under the most unfavorable conditions as to all of the loads which affect this variation. In order to establish a safe criterion for this maximum permissible variation (for example v per cent), it will be necessary to consider both shop and design practice.

In making a given setup the welding operator adjusts his controls so as to obtain a specified minimum weld strength. The structural designer, on the other hand, is given a design strength 15 per cent below this shop test strength, in order to allow not only for voltage variations but also for several other variations which may not be negligible except with unusual care in the technique of operation.

In the following quantitative analysis these other variations are neglected, and it is assumed that weld strength for the chosen timing (assumed to be constant) is proportional to the square of the voltage impressed upon the primary winding of the welding transformer. This last assumption is true only when the tech-

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The authors wish to express their appreciation to George Stocker and L. N. Blugerman for their contributions to the practical design and layout of the feeder; to Bartram Kelley for his early work on the probability problem; to the Associated Factory Mutual Insurance Company for its suggestions concerning safety and fire prevention; to the Railway Industrial Engineering Company which overcame wartime procurement difficulties to provide 18,000 feet of the feeder in record time, and finally to the Edward G. Budd Company which provided the opportunity for this development.

nique specified to the shop yields a middle range or optimum weld. Let us assume the worst condition, namely, that the test weld which yields the desired minimum weld strength was made when the voltage at the primary winding of the welding transformer was at its maximum value or at the top of its range. Assume also that some of the production welds on this job were made at the bottom of this welding range, namely v per cent below that for the test weld. In this case the strength of these latter welds will be $(100 - v)^2/100^2$ per cent or approximately $2v$ per cent below that of the test weld. Thus, if 15 per cent is the maximum permissible weld strength deficiency below that of the shop test weld, v should not be more than eight. Common practice ranges from eight to ten per cent. Please note, however, that v is defined as the per cent variation of voltage at the primary winding of any welding transformer under load, rather than what generally is understood as the maximum voltage regulation, namely, the per cent variation from no load to maximum load. This distinction is clear in Navy specifications[1] which read as follows:

> "The voltage regulation. . .shall be such that the voltage at each welding control shall not vary more than ten per cent due to causes other than the load in question. . . this ten per cent variation includes variation in power-supply voltage, plus the voltage regulation in transformers, plus the line drop in feeders."

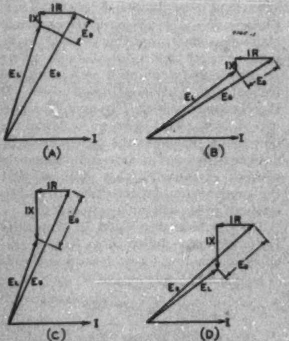

Figure 1. Vector diagrams

A and B—Rectangular bar bus-type feeder carrying 30 per cent and 80 per cent power factor load. Numerical drop is less than IR drop in A. C and D—cable feeder of same cross section carrying 30 per cent and 80 per cent power factor load

E_L—load voltage

E_S—source voltage

E_D—numerical drop

Even assuming that this ten per cent variation is a safe over-all criterion, it is obvious that the permissible maximum variation in the welding feeder drop must not be more than four or five per cent to allow for the foregoing items a, b, and c. Thus, if we assume no other method of voltage control, it would be necessary so to design the feeder as to limit to five per cent the *variation* of feeder drop as between that corresponding to a single welder and that corresponding to the maximum probable number of welders whose current cycles would overlap by a significant amount frequently enough to produce an undesirable percentage of understrength welds. It is because of the difficulty of meeting this criterion on a feeder carrying a large number of welders operating on a high production schedule, that several devices have been developed for maintaining constant current. Nevertheless, since these devices require one or two, and in some cases more, cycles to get under way, it it still highly desirable to reduce the variation in the feeder drop to the lowest possible point consistent with a reasonable installation cost. The cost of a defective weld may be great, and although there are other causes for defects, there can be no doubt that voltage variation is a major offender. The other sources of trouble can be minimized by proper training of the welding operators and by maintenance of equipment.

With this background the following criterion is proposed:

> That the welding being done be investigated to determine what proportion of the welds made reasonably can be allowed to be understrength welds. This, of course, will be dependent on the nature of the structure under fabrication.

Since the maximum variation in feeder drop is the difference in drop between that due to the maximum number of machines welding at the same time and that due to only one machine, it is obviously necessary to know the probability of any given number of machines striking at the same time or sufficiently near thereto to cause an objectionable overlap of their current flow intervals. The maximum permissible overlap must be specified and is a matter of judgment although it can be roughly computed. Ordinarily, an overlap of less than 25 per cent is not objectionable. It should be noted, however, that most timing devices are adjustable only to an integral number of cycles and that the overlap therefore must be rated in cycles. Therefore, the minimum overlap will be one cycle, which would be objectionable for a weld of less than four cycles.

This is discussed at more length in Appendix II, which deals with the quantitative application of the theory of probability to the problem.

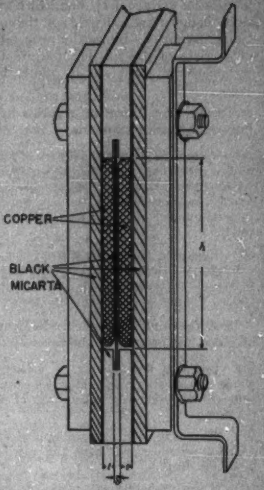

Figure 2. Cross section of parallel bar bus-type feeder

Reactance of this type of feeder is practically negligible

Assume that the feeder is supplying current to n welders. Assume that each of these machines takes the same current and operates on the same duty cycle. Although these last assumptions never hold in practice since the welders are not only of varying sizes but also are adjusted at different times for different currents and duty cycles, any attempt to take account of these varying and complex combinations would render practically impossible the employment of the theory of probability which is essential to the solution of this problem. However, the assumption of an average current and average duty cycle will yield results of sufficient accuracy. In fact, we can assume a current and duty cycle somewhat above the average which will yield conservative results.

Assume next that a very small number of welds, for example, one out of every Q may be permitted to be understrength. This means that there must not be more than one chance in Q of having more than the permissible overlap of the current-flow intervals of a number of machines sufficient to produce a feeder voltage drop of five per cent more than that of a single machine. Suppose that the feeder is designed to carry the current of s_0 machines at once with a voltage drop of five per cent. Then s_0+1 machines will produce a differential voltage drop of five per cent. That is a drop of five per cent more than that of one machine. A coincidence of s_0+2 machines will produce an objectionable voltage drop, one which may result in understrength welds on the coinciding machines. Hence the probability of overlaps of the specified nature of s_0+2 machines must not be more than $1/Q$.

The problem therefore, resolves itself into the following steps:

(*a*). Determine first by the theory of probability the number $s=s_0+2$ machines whose current flow intervals are likely to overlap by 25 per cent or the next smaller integral number of cycles, once in every Q welds.

(*b*). Compute the total current for s_0 machines and design the feeder so that the voltage drop resulting when this current is carried will not be more than five per cent.

If, on the other hand, the feeder design be given, the process will be reversed:

(*a*). Compute the number s_0 machines, which striking at once, would give a current sufficient to produce a feeder drop of five per cent.

(*b*). Then determine the probability that $s=s_0+2$ machines will have the specified minimum overlap.

(*c*). If this probability is such as to produce an undesirably large percentage of understrength welds, either the feeder would have to be redesigned or one of the automatic control devices installed on each welder.

Preliminary Design Considerations

It becomes apparent from the foregoing discussion that in order to analyze the problem mathematically and so that the mathematical solution will be of some practical value, a certain amount of data must be collected, estimated, or assumed.

This is a vital part of the solution of the problem since the mathematical analysis is only as good as the assumptions on which it is based. These considerations are:

1. *Distribution voltage.* From the standpoint of impedance drop in the feeder the distribution voltage should be as high as possible, since for a given feeder the per cent drop varies inversely as the voltage squared. There are other factors to be considered, however. In many cases the voltage is fixed by previously installed equipment. If one does have some freedom in the choice of a voltage, he is limited by commercially available products, prejudice, or safety.

2. *Substations.* The number, arrangement, and capacity of substations must be known or assumed in order that the area to be supplied by the proposed feeder and the length of the feeder may be determined.

3. *Type and location of load.* The plant layout probably is fixed by the assembly procedure; hence in general the type of welding to be done in a given location also is likely to be more or less fixed. From these data an estimate must be made of the number of machines which will be on a given feeder, the average rate at which welding will be done, and the average current and number of cycles duration of each weld.

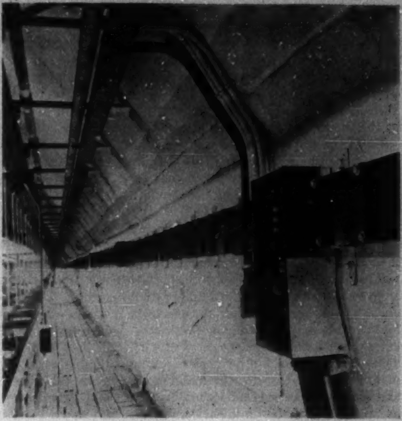

Figure 3. An installation of bus-type feeders in a recently built plant

Note riser of same construction, and flexible jumper in junction box

4. *Proportion of understrength welds.* The type of structure must be studied and a reasonable percentage of understrength welds decided upon or, if lockout devices are employed, a figure must be set for the number of lockouts to be allowed per machine in a given period.

With this information available it is possible to state the specifications more directly in electrical terms, that is, in terms of current carrying-capacity, maximum tolerable impedance, and insulation requirements.

The current-carrying-capacity, from a thermal standpoint usually will be subordinate to the regulation requirements, unless the feeder is supplying seamwelders or other high-duty-cycle equipment. In any case the thermal capacity must be checked to determine whether or not the thermal limitations rather than the impedance will be the basis of the design.

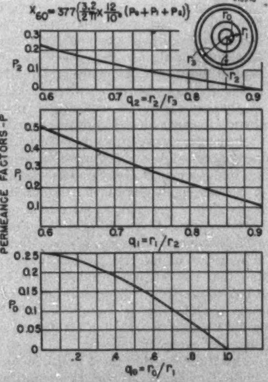

Figure 4. Permeance factors for the determination of approximate reactance at 60 cycles in ohms per loop foot of concentric cables or busses

Probability and Impedance

A criterion of good voltage regulation has been established, namely: that for a given type of production a very small percentage of the welds made on each machine reasonably can be allowed to be understrength, and that jobs with severe requirements be equipped with a suitable lockout or compensating device. It then becomes necessary to apply the theory of probability in order to determine how many machines the feeder must be designed to accommodate simultaneously without excessive voltage drop so that the predetermined percentage of understrength welds will not be exceeded.

The formula developed in Appendix II for the probability of at least s machines coinciding for at least k cycles is

$$P_{\geq s}=\frac{1}{(a+b)^{n-1}}\sum_{h=0}^{n-1-s'}(2a-2k+1)^{n-1-h}\times$$

$$(b-a+2k-1)^{h}\cdot\frac{(n-1)!}{(n-1-h)!h!}\quad a\gtreqless b \qquad (1)$$

where:

a is the average of the number of on cycles of all the machines on the feeder
b is the average of the number of off cycles of the machines
$s=s'+1$ is the number of machines coinciding for at least k cycles
s' is the number of machines coinciding with a given machine

n is total number of machines on the feeder
h is the variable of summation

If no more than one weld in Q is allowed to be understrength, the feeder must be designed to carry simultaneously with a drop of no more than five per cent, a number of machines s_0 such that the probability of at least $s_0 + 2$ machines coinciding for at least k cycles is equal to or less than $1/Q$. That is,

$$P_{\geq s_0+2} \leqq \frac{1}{Q} \tag{2}$$

The procedure, therefore, is to determine from equation 1 a value of $s=s_0+2$ such that equation 2 is satisfied. Under these conditions there are not likely to be coincidences of more than $s_0 + 2$ machines, of objectionable duration, more often than once for every Q welds. The current which will flow when s_0 machines are on together will be $I_s=s_0I_a$ where I_a is the rms value of the primary welding current of one machine which flows for a cycles. The impedance Z_f of the feeder must be such, therefore, that I_sZ_f is equal to or less than five per cent of the feeder voltage.

It has been possible by proper design to reduce the reactance of the welder supply feeder to a point where it becomes practically negligible. Since resistance welding loads usually have a low power factor and since the power factor of the bus can be made very close to unity, the voltage drop often will be less than the IR drop of the feeder. The regulation therefore, can be made as low as desired by increasing the amount of copper, until an economically justifiable limit is reached. The flat bar bus has a reactance which is less than that of a solid-core concentric cable of the same section, and is in general lower than that of a concentric tubular bus of practical design. A tubular bus of large enough diameter and of thin enough copper could be made with a lower reactance; however, the practical limitations of size, difficulty of arranging taps, strength, and extra cost do not justify this type of construction, since the reactance of the flat bar bus can be made small enough to be negligible.

The 60-cycle reactance of a bus of the type shown in Figures 2 and 3 is developed in Appendix III and is shown to be approximately:

$$X'=\frac{144.5}{10^6}\left[\frac{\delta}{h}+\frac{2}{3}\frac{t}{h}\right] \text{ohms per loop foot} \tag{3}$$

where

δ is distance between faces of the bars in inches
h is the height of each bar in inches
t is the thickness of each bar in inches

The 60-cycle reactance of concentric cables or tubular busses can be determined from the permeance factors P_0, P_1, P_2, which are plotted in Figure 4 as a function of the ratios of the various radii.

Figure 5 is a comparison of the impedance of a few of the many possible arrangements of conductors.

Probability and Temperature Rise

The problem of predetermining the temperature rise in a bus feeder carrying an equivalent continuous current I_e is discussed in Appendix IV. It is necessary, however, to apply the theory of probability in order to find this equivalent continuous current. It is pointed out in Appendix II that although it might seem that the equivalent continuous current of n machines were n times the equivalent current of one machine, this is false logic, since the current obtained by this method actually produces the maximum possible heating effect, which corresponds only to a coincidence of machines that is extremely improbable.

The most probable equivalent continuous current is shown in Appendix II to be

$$I_e=I_a\sqrt{n!\sum_{s=0}^{n}\frac{s^2d^s(1-d)^{n-s}}{(n-s)!s!}} \tag{4}$$

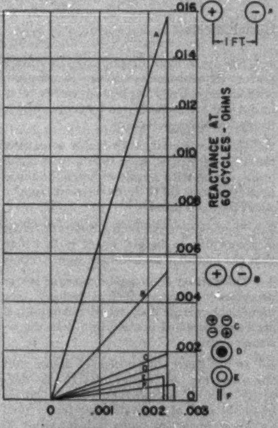

Figure 5. Impedance diagrams of various feeders—100 loop feet

A—One 1,000,000 circular mils per leg—1 foot centers; *B*—1,000,000 circular mils per leg close; *C*—Two 500,000 circular mils per leg interlaced; *D*—1,000,000 circular mils per leg—concentric solid core; *E*—concentric tubular feeder, inner conductor—inside diameter=1.72 inches, outside diameter=2 inches. Outer conductor—inside diameter=2.3 inches, outside diameter=2.5; *F*—$^3/_{16}$-inch by 4-inch parallel bar bus, $^1/_{16}$-inch space between faces

where

I_s is the rms value of primary welding current which flows for a cycles
n is the number of machines on the feeder
s is the number of machines in the general term of the summation
d is the duty cycle or the probability of any one machine including any one cycle

In general I_s rather than I_e will be the current which determines the feeder section. However, both must be calculated in order to be sure that neither the permissible impedance drop nor the permissible temperature rise is exceeded.

As a result of these calculations the cross section required for the flat bar bus illustrated in Figure 2 is about 990 circular mils per ampere. This bus consists of two pieces of $^3/_{16}$-inch by 4-inch copper, separated by $^1/_{16}$-inch of black Micarta and covered with $^1/_4$-inch sheets of the same material. For conductors of this thickness the circular mils per ampere is practically independent of the height, provided h is at least $20t$.

If no forced cooling is used, all the heat developed in a concentric tubular bus must be dissipated from the outer conductor. Under these conditions a bare concentric tubular bus of practical design insulated with the same material between conductors would be rated at approximately 1,400 or 1,500 circular mils per ampere.

Hence, the concentric tubular feeder requires about 35 per cent more copper if it is to be limited to the same temperature rise as the flat bar bus.

Mechanical Forces

A conductor carrying a current in a magnetic field is subject to a mechanical force. The clamps and supports for a bus therefore must be so placed and of such strength as to prevent any objectionable distortion in the structure. Under normal operating conditions the forces are seldom of much consequence; however, these forces may be sufficient to destroy the feeder in the event of a short circuit, unless the supports are designed to withstand these stresses. It has been shown in Appendix V that maximum deflection, neglecting the support offered by the insulating cover is

$$d_{max}=\frac{5.54l_s^4I_{ss}^2}{h^2t^3\times10^{14}} \text{ inches} \tag{5}$$

or that the maximum distance between clamps in terms of a maximum allowable deflection is

$$l_s=\frac{0.652d_{max}^{1/4}h^{1/2}t^{3/4}\times10^4}{I_{ss}^{1/2}} \text{ inches} \tag{6}$$

where

d_{max} is the maximum permissible lateral deflection of either conductor in inches.
l_s is the distance between supports.

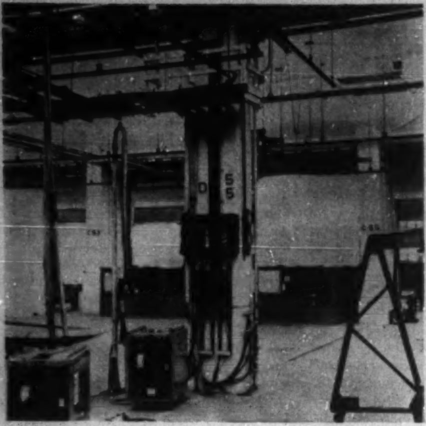

Figure 6. An installation of bus-type feeder

t is the thickness of one conductor.

I_{ss} is the rms value of the worst short-circuit current the feeder is likely to encounter.

It is equation 6 which limits the ratio of h/t. From all other viewpoints this ratio should be as large as possible, however, when t becomes very small, the number of clamps required becomes excessive.

If the expansion of the bus due to temperature rise is restrained, there will be longitudinal stresses in the structure which might be of sufficient magnitude to cause buckling. In order to circumvent this source of trouble, the feeders have been built in subassembled sections and connected with semiflexible jumpers, an example of which is shown in Figure 5. The frictional restraint offered by the clamps, is not sufficient to cause excessive stresses in the 20-foot lengths which are used.

Operating Experience With Rectangular Feeder

Even though the insulation thickness between conductors is less than in conventional design, there has been no difficulty from a safety standpoint. The bus-type feeder has withstood tests of 5,000 volts between conductors and to ground satisfactorily. Installations have been examined by the Associated Factory Mutual Fire Insurance Companies which consider that the fire hazard has been minimized and that the welding bus is acceptable for general industrial service.

A number of these low-reactance rectangular bar bus-type feeders have been installed and are now in operation. In a recently built plant where this type of feeder is used almost exclusively, there has arisen absolutely no trouble as a result of poor voltage regulation.

In addition to its electrical advantages, this type of construction lends itself to prefabrication, is mounted easily, and can be tapped readily for branch loads. These details are shown in Figures 6 and 7.

Conclusions

1. The problem of distributing low-voltage power to resistance-welding installations without excessive voltage drop can be minimized and often eliminated by proper design of the low-voltage feeders.

2. The most satisfactory feeder consists of two bars of copper, each with a rectangular cross section with a ratio of height to thickness of at least 20 to 1, and spaced $^1/_{16}$ inch apart between faces.

3. The reactance of such a bus-type feeder is practically negligible, lower in fact than that of concentric cables of the same section.

4. The voltage drop can be made as low as necessary by using sufficient copper. The numerical voltage drop when welding loads are being supplied is often less than the resistance drop.

5. It has been shown clearly above that the criterion of feeder design is definitely that of voltage regulation. Ont his basis the cost of a parallel bar feeder such as discussed in this paper will be about 20 per cent more than that of an equivalent cable feeder with the conductors arranged for minimum reactance. The cost of an equivalent concentric tubular feeder, in general, will be higher than that of a parallel bar feeder.

Appendix I

Nomenclature

PROBABILITY

a = average number of cycles duration of each weld

b = average number of cycles between welds on each machine

h = variable of summation in the general term

n = number of machines supplied by the feeder

$m = n-1$

s = number of machines coinciding for a period of at least k cycles

$s' = s-1$, number of machines coinciding with a given one

s_p = the most probable number of machines on during any one preselected cycle

k = number of cycles for which a given machine could be overlapped by all of the other machines and still produce a satisfactory weld

$P_{\geq s}$ = probability of *at least* s machines coinciding for at least k cycles

p = probability that any one machine will have k of its a on cycles occur during any preselected equally likely period of a successive cycles

p_1 = probability that any one machine will be on during any one preselected cycle

P_{ns} = probability that *exactly* s machines will be on during any one preselected cycle

GEOMETRIC

h = height of bus bars in parallel rectangular bar feeder in inches

t = thickness of each bus bar in parallel rectangular bar feeder in inches

ti = thickness of insulation on the outside of bus in inches

δ = thickness of insulation between conductors

r_0 = inner radius of inner conductor for tubular bus in inches

r_1 = outer radius of inner conductor for tubular bus in inches

r_2 = inner radius of outer conductor for tubular bus in inches

r_3 = outer radius of outer conductor for tubular bus in inches

x = distance from origin to part of rectangular bus under consideration

l_o = distance between clamps in inches

d_{max} = maximum permissible deflection of conductors in inches

ELECTRICAL

Φ = flux lines

ψ'' = flux linkages per ampere inch

L' = inductance of one foot and return in henrys per loop foot

X' = reactance of one foot and return in ohms per loop foot

i = total current in each conductor at any instant

I_c = equivalent continuous current in amperes

I_a = primary welding current of one machine (the rms current which flows for a cycles) in amperes

ρ'' = resistivity of copper in ohms per circular mil inch. (Approximately equal to 1 for electrical copper at 60 degrees centigrade.)

r' = resistance of one inch of the copper in question in ohms per inch
r_f = total feeder resistance
Z_f = total feeder impedance
$\odot$ = circular mils per ampere
d = duty cycle $a/(a+b)$

THERMAL

P' = rate of heat generation in one conductor in watts per inch
k = rate of heat dissipation from surfaces such as those encountered in electrical distribution equipment, in still air at usual operating temperatures in watts per degrees centigrade per square inch
R' = thermal resistivity of the insulating material through which heat must flow in thermal ohm inches (degrees centigrade per watt per cubic inch)
$\mathfrak{R}$ = thermal resistance of the path in question in thermal ohms
θ_a = ambient temperature in degrees centigrade
θ_c = temperature of copper in degrees centigrade
$\Delta\theta_i$ = temperature drop in outer insulation in degrees centigrade
$\Delta\theta_e$ = maximum temperature rise in any part of bus in degrees centigrade

MECHANICAL

F' = force on conductors in pounds per linear inch
M = moment of inertia of section of beam about neutral axis in inch^4
E = modulus of elasticity

Appendix II. Probability of Coincidence of Welding Machines

There are two distinct problems to which the theory of probability should be applied in the design of welding feeders. The first is to determine the maximum tolerable impedance; the second, to find the equivalent continuous current. In both of these problems certain simplifications are necessary.

The variety of welding currents, firing angle settings, power factors, and timings encountered on a number of machines on an assembly floor is so great, that to attempt to include these factors in an analysis is absolutely prohibitive. The simplifying assumptions are listed below.

The term *cycle* is used here in its a-c sense. By the term *period* is meant the time which elapses between the beginning of the flow of welding current in one machine and the beginning of the flow of welding current for the next weld on the same machine. The term *probability* is used for the ratio of the number of ways an event can occur to the sum of the number of ways that event either can occur or fail to occur.

Assumptions:

1. That the current in the primary winding of the welding transformer when welding is the same as the current of every other machine, that it has an rms value of I_a amperes, while it flows, and that it flows for a cycles.

2. That there are, on the average, b cycles between welds on each machine. The period is, therefore, $a+b$ cycles, and the average duty cycle of each machine is $a/(a+b)$.

3. That the machines are operated entirely at random, but are synchronous, and always fire at the beginning of a cycle and start when the voltage is of the same polarity.

4. That an overlap of current-flow interval of a sufficient number of machines to produce an excessive voltage drop, as previously defined in the body of the paper, is objectionable only if the overlap is at least 25 per cent of the current-flow interval or the next smaller integral number of cycles thereto.

Figure 7. Expansion joint between prefabricated sections of feeder

So far as the impedance drop is concerned, the first problem is to find the probability of at least s of the total n machines on the feeder, coinciding for a duration of at least k cycles.

Suppose we select one of the machines as a reference. Any set of a successive cycles out of any period of $a+b$ cycles is equally likely to be the series of a cycles which this reference machine will include in its on period.

The problem, therefore, is to find the probability of at least $s' = s-1$ of the remaining $m = n-1$ machines having at least k of their on cycles occur during any one of the equally likely intervals of a successive cycles which represent the on time of the reference machine.

Let p be the probability of any one of the m machines having at least k of its on cycles occur during any one of the equally likely intervals of a cycles. It is a theorem, that if the probability of the occurrence of an event on a single trial is p, the probability that it will occur at least s' times in the course of m independent trials is:

$$P_{\geq s} = p^m + {}_mC_1p^{m-1}(1-p) + {}_mC_2p^{m-2}\times (1-p)^2 + \ldots + {}_mC_hp^{m-h}(1-p)^h + \ldots + {}_mC_{m-s'}p^{s'}(1-p)^{m-s'} \qquad (7)$$

$$P_{\geq s} = \sum_{h=0}^{m-s'} \frac{m!}{(m-h)!h!} p^{m-h}(1-p)^h \qquad (8)$$

It obviously makes no difference whether we make m independent trials with one machine or one trial with each of m independent machines so long as the basic probability of the occurrence of the event (which in this case is the overlapping of a given set of a cycles by at least k cycles) is the same for each machine.

Hence, $P_{\geq s}$ is the probability of at least s' machines out of a total of m machines overlapping a given machine by at least k cycles or is the probability of at least s machines of the total n coinciding for an interval of at least that duration.

The basic probability p has not yet been determined, but can be found by inductive reasoning from the results of simple cases.

Consider two machines, which are on for three cycles and off for seven. Suppose the reference machine is on during cycles 5, 6, and 7 in the diagram of Figure 8. Any other machine can come on in any one of ten different positions; ten is the total number of ways the event (overlap of cycles 5, 6, and 7) can either occur or fail to occur. If $k=1$ a machine striking in any one of five positions 3, 4, 5, 6, and 7 would constitute an occurrence, hence p in this case is 5/10. If k is two cycles then positions 4, 5, and 6 are the only positions in which the event could occur so that the probability is 3/10. If k is three cycles the probability will be 1/10.

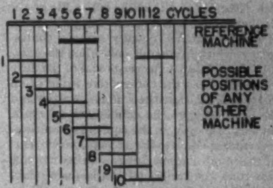
1 2 3 4 5 6 7 8 9 10 11 12 CYCLES
REFERENCE MACHINE
POSSIBLE POSITIONS OF ANY OTHER MACHINE
1
2
3
4
5
6
7
8
9
10

Figure 8. Positions of overlapping machine for a special case, 3 cycles on—0.3 duty cycle

Positions 3, 4, 5, 6, and 7 constitute overlaps of the reference machine by at least one cycle. Positions 4, 5, and 6 constitute overlaps of at least two cycles; position 5 an overlap of at least 3 cycles

From this type of diagram Table I might be constructed for $(a+b)=10$ and for various values of a and k.

These probabilities can be expressed by the equation:

$$p = \frac{2a-2k+1}{a+b} \quad \begin{pmatrix} 2a-2k+1 \geqq 0 \\ a \gtreqless b \end{pmatrix} \qquad (9)$$

Substituting in equation 9,

$$P_{\geq s} = \frac{1}{(a+b)^m} \sum_{h=0}^{m-s'} \frac{m!}{(m-h)!h!} \times (2a-2k+1)^{m-h}(b-a+2k-1)^h$$

$$P_{\geq s} = \frac{1}{(a+b)^{n-1}} \sum_{h=0}^{n-1-s'} \frac{(n-1)!}{(n-1-h)!h!} \times (2a-2k+1)^{n-1-h}(b-a+2k-1)^h \quad \text{note } (0)! = 1 \qquad (10)$$

An example of the use of this formula appears in Appendix VI.

Probability and the Thermal Problem

There is another aspect of the problem of feeder design to which the theory of probability should be applied, namely, to find the equivalent continuous current which causes heating of the feeder. It might seem obvious that the equivalent continuous current of n machines is n times the equivalent continuous current of one machine, but this is not true in this case. Consider for a moment the remote possibility of all machines repeatedly starting to weld at the same instant. This would be the worst possible case from the standpoint of feeder heating. For this case the rms current which flows for a cycles is nI_a and the equivalent continuous current is $nI_a\sqrt{d}$. The equivalent continuous current for one machine is $I_a\sqrt{d}$, hence the computation of n times the equivalent continuous current of one machine would indicate a heating effect that is the maximum possible, corresponding to the exact coincidence of all the machines on the feeder—an event which may occur only a few times, if at all, in the entire life of the feeder. That is not sound design practice. There is another equally remote contingency, which is possible only when the number of machines is the reciprocal of the duty cycle. This is the consecutive firing of all machines, so that there is no overlap and no time between the conduction intervals. In this case only, is the equivalent continuous current equal to the average rms current. If the number of machines is either more or less than the reciprocal of the duty cycle, the equivalent continuous current will be larger than the average rms current.

The most probable current must lie between these two extremes. Consider the probability of any one machine being on during any one preselected cycle. If the machines are each on for a and off for b cycles this probability is

$$p_1=a/(a+b)$$

or

$$p_1=d$$

The probability that *exactly* s out of a total of n machines will be on during that cycle is

$$p_{ns}=\frac{n!}{(n-s)!s!}\,d^s(1-d)^{n-s} \qquad (11$$

If this equation be solved for P_{ns} for all values of s from $s=0$ to $s=n$, a distribution curve P_{ns} vs. S can be plotted. The peak of this curve occurs at the expected number of machines e. It can be demonstrated that $e=np_1$ is the probable average number of machines which will be on during the preselected cycle. The most probable number of machines on during this cycle is the nearest integer to $e=np_1$. The product eI_a is the probable average current. However, for the purpose of calculating temperature rise we must have the current which is the square root of the average squared current.

The current during the preselected cycle which is any cycle and therefore representative, corresponding to exactly s machines being on during that cycle, is $I_s=sI_a$, and the rate of heat generation is proportional to $I_s^2=s^2I_a^2$. Consider a typical distribution curve: P_{ns} vs. s (Figure 9). The overlap of the preselected cycle by exactly 1, 2, 3, 4, ... n machines includes all possible occurrences, hence the sum of the probabilities $P_{n1}+P_{n2}+P_{n3}+\ldots+P_{nn}$ is unity. Since the area under each step of the curve in Figure 9 is proportional to the probability, the total area under this curve is unity. That is,

Table I. Probabilities

Cycles on	K=1	2	3	4	5
3	5/10	3/10	1/10	0	0
4	7/10	5/10	3/10	1/10	0
5	9/10	7/10	5/10	3/10	1/10

$$\sum_{s=0}^{n}P_{ns}=1$$

Therefore, the probability P_{ns} represents the proportion of any time interval during which a current I_s is likely to flow. Let T be the length of any time interval. Then

$\Delta T_s=P_{ns}T=$ the portion of that interval during which the current $I_s=sI_a$ is likely to flow.

The heat developed during the time interval ΔT_s is

$$\Delta J_s=I_s^2r_fP_{ns}\Delta T_s=I_a^2r_f\Delta T_s s^2P_{ns}$$

where r_f is the resistance of the feeder. The total heat developed in the interval T is

$$J=\sum_{s=0}^{n}\Delta J_s=I_a^2r_f\sum_{s=0}^{n}s^2P_{ns}$$

if I_e is the equivalent continuous current.

$$J=I_e^2r_f$$

hence:

$$I_e=I_a\sqrt{\sum_{s=0}^{n}s^2P_{ns}} \qquad (12)$$

$$I_e=I_a\sqrt{n!\sum_{s=0}^{n}\frac{s^2p_1^s(1-p_1)^{n-s}}{(n-s)!s!}}$$

Since

$$p_1=d=a/(a+b)$$

hence

$$I_e=I_a\sqrt{n!\sum_{s=0}^{n}\frac{s^2d^s(1-d)^{n-s}}{(n-s)!s!}}\ \text{amperes} \qquad (13)$$

is the most probable equivalent continuous current.

Appendix III. Inductance of Rectangular Parallel Bus Bars

The inductance of a circuit may be defined in several ways. The most basic definition is the induced electromotive force divided by the rate of change of current. If the electromotive force and current are expresed in volts and amperes, the corresponding inductance will be in henrys.

For our present purpose a more convenient definition is the flux linked with the circuit, multiplied by the number of turns in the circuit, and divided by the current.

If the flux is expressed in maxwells and the current in amperes, the inductance in henrys will be equal to the flux linkage per ampere multiplied by 10^{-8} or

$$L=\frac{N\Phi10^{-8}}{I}$$

Since the relation of Φ to N and I involves the reluctance of the magnetic circuit which links the electric circuit, it is obvious that if the magnetic circuit includes any magnetic material with its variable permeability, the inductance will not be constant. Fortunately, in our case we may assume that there is no magnetic material in the magnetic circuit linked with the bus bars in question. Also, since it is a one-turn circuit, N drops out.

In a uniform straight magnetic field in nonmagnetic material, the flux density in maxwells per square inch is equal to 3.2 times the magnetic potential gradient in ampere turns per inch of length, or $B''=3.2\ NI/l''$.

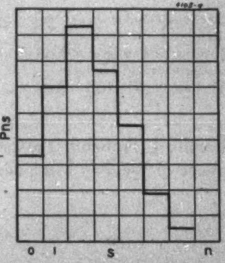

Figure 9. Typical distribution curve, showing probability of exactly s machines including a given cycle

If we assume a direct current uniformly distributed across the cross section of each bus bar and in opposite directions in the two bus bars, the actual flux distribution will look something like that shown in Figure 10 and although this can be computed and plotted with reasonable accuracy, it is a tedious process. It will not be attempted for three reasons:

1. The geometry of our problem is such as to make possible a very great simplification without serious loss of accuracy. This simplification consists of the assumptions that:

   (*a*). All of the flux passing through and between the two bus bars, is parallel thereto as shown in Figure 11.

   (*b*). The reluctance of the return magnetic circuit through the surrounding air or nonmagnetic material is practically negligible for our present purpose.

   Although this may not seem legitimate at first glance, it has been checked both by accurate theoretical calculations and by measurement.

The equivalent length of this external return circuit is much greater than that of the path between and through the bus bars, but its equivalent cross section is larger by a much greater factor. That is, the reluctance of the external part of the magnetic circuit is a small fraction of that of the internal part.

Since the bus is supposed to be long, straight, and uniform, the inductance of each inch of length will be the same as that of every other one. We will compute therefore the inductance of one inch length of a single bar, remembering that with our symmetrical geometry, half of the total flux will link with each bar.

The inductance per inch length of a single bar is thus its flux linkage per ampere, multiplied by 10^{-8}. Moreover, the assumption of parallel flow within and between the two busses yields a higher internal reluctance than the actual distribution within that region, which partly balances the neglect of the external reluctance. Thus the inductance calculated on this basis will be conservative or a little higher than the actual inductance, but probably more accurate than can be measured by an ordinary test with voltmeter, ammeter, and wattmeter.

The approximations involved in the two aforementioned simplifying assumptions are valid only when the height of the bus bar is large as compared with its thickness. The ratio here employed is about 20 to 1 and should not be much less on this score.

There is an interesting corollary to this condition of negligible return-circuit reluctance, namely, that the presence of magnetic material such as steel even in the near vicinity of the feeder would not alter appreciably its inductance. In other words, such a bus could be supported directly on the web of a steel channel or I beam.

2. Owing to the familiar skin-effect phenomenon, the current will not be distributed uniformly across the bar thickness, but will have a higher density on the face of each bar. Although this affects both the apparent resistance and inductance, it is by a negligible amount for 60 cycles and a thickness not greater than 1/4 inch. Both effects would increase with increase of thickness or frequency.

3. It is certain that the foregoing approximations employed hereafter will yield an inductance close enough for all practical purposes.

Calculation of Inductance

Since we are assuming uniform current distribution over the conductor section, the result might be referred to as the d-c or zero frequency inductance. Although even from this point of view, the results are only approximate as previously explained, but on the high side.

Referring to Figure 11, consider the inductance per inch length of a single bar due to that part of the between-bar flux which links with the bar in question, namely, one half.

Assuming one ampere in each bar, assuming the flux parallel to the plane of the bar, and neglecting the reluctance of the return magnetic circuit, the magnetic potential gradient in the between-bar space measured in ampere turns per inch length of the magnetic path, then will be $1/h$. The corresponding magnetic density in maxwells per square inch will be

$$B_t = \frac{3.2}{h}$$

and the corresponding flux linkage per ampere inch of bar length

$$\psi_t'' = \frac{3.2}{h}\frac{\delta}{2}$$

Thus the inductance in henry per inch length of a single bar and only for the between-bar flux is

$$L_t = \psi'' 10^{-8} = \frac{1.6\delta}{10^8 h}$$

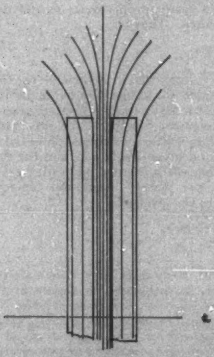

Figure 10. Approximate flux distribution around parallel-bar feeder

Although the actual flux map could be made, it is a tedious process, because of the proportions of the feeder

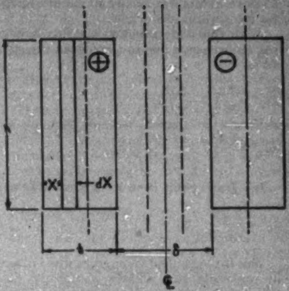

Figure 11. Flux distribution assumed for the purposes of calculation

This approximation yields results often more accurate than those obtainable by careful measurement with voltmeter, ammeter, and wattmeter

Consider next the inductance due to that part of the flux which passes through the bar section. The flux which passes through the strip dx links only with that part of the current outside of this strip, that is, with a current equal to $(t-x)/t$.

Thus the flux density in this strip will be

$$B_c = \frac{3.2}{h}\left(\frac{t-x}{t}\right)$$

and the flux in this strip will be

$$d\Phi = B_c dx = \frac{3.2}{h}\left(1-\frac{x}{t}\right)dx$$

The fact that this flux links with only a part of the current, or when alternating, induces a voltage in only that part of the conductor section with which it links usually is dealt with by assuming that instead of inducing this voltage in only a part of the conductor, it induces in the whole conductor or in the circuit as a whole a fraction of this voltage which is the fraction of the conductor with which it links. This assumption is warranted only in those cases where the skin-effect distortion is negligible. More-

Table II

A h	B (n−1−h)	C $(2a-2k+1)^{n-1-h}$	D $(b-a+2k-1)^h$	E $\frac{(n-1)!}{(n-1-h)!h!}$	F C×D×E	G S′	H S	I n−1−S′	J Sum of First (n−s′) Terms in Column F	K $P_{\geq s}$	L $P_{\geq s}$
0	14	1.680×10^{17}	1.0	1.000	Negligible	14	15	0	Negligible	Negligible	
1	13	9.80×10^{15}	1.030×10^{2}	1.400×10	Negligible	13	14	1	Negligible	Negligible	
2	12	5.70×10^{14}	1.060×10^{4}	9.100×10	Negligible	12	13	2	Negligible	Negligible	
3	11	3.40×10^{13}	1.091×10^{6}	3.640×10^{2}	Negligible	11	12	3	Negligible	Negligible	
4	10	2.00×10^{12}	1.124×10^{8}	1.002×10^{3}	Negligible	10	11	4	Negligible	Negligible	
5	9	1.17×10^{11}	1.159×10^{10}	2.000×10^{3}	0.0002×10^{28}	9	10	5	0.0002×10^{28}	0.00001	1/100,000
6	8	6.90×10^{9}	1.193×10^{12}	3.000×10^{3}	0.002×10^{28}	8	9	6	0.0022×10^{28}	0.00017	1/5,900
7	7	4.10×10^{8}	1.230×10^{14}	3.430×10^{3}	0.017×10^{28}	7	8	7	0.0192×10^{28}	0.0015	1/666
8	6	2.40×10^{7}	1.268×10^{16}	3.000×10^{3}	0.091×10^{28}	6	7	8	0.1100×10^{28}	0.0086	1/115
9	5	1.41×10^{6}	1.305×10^{18}	2.000×10^{3}	0.368×10^{28}	5	6	9	0.478×10^{28}	0.0374	1/26
10	4	8.30×10^{4}	1.341×10^{20}	1.002×10^{3}	1.120×10^{28}	4	5	10	1.598×10^{28}	0.1250	1/8
11	3	4.90×10^{3}	1.382×10^{22}	3.640×10^{2}	2.460×10^{28}	3	4	11	4.058×10^{28}	0.317	1/3.1
12	2	2.88×10^{2}	1.425×10^{24}	9.100×10	3.740×10^{28}	2	3	12	7.798×10^{28}	0.610	1/1.6
13	1	1.70×10	1.468×10^{26}	1.400×10	3.500×10^{28}	1	2	13	11.298×10^{28}	0.880	1/1.1
14	0	1.00	1.510×10^{28}	1.000	1.510×10^{28}	0	1	14	12.808×10^{28}	1.0	1 (check)

over, even then the resulting inductance due to this internal flux is only a kind of average for the current as a whole.

With this understanding the flux linkage per ampere inch due to the flux in the strip dx is:

$$d\psi_c'' = \frac{3.2}{h}\left(1-\frac{x}{t}\right)^2 dx$$

$$d\psi_c'' = \frac{3.2}{h}\left(1-\frac{2x}{t}+\frac{x^2}{t^2}\right)dx$$

Integrating this from $x=0$ to $x=t$ we get

$$\psi_c'' = \frac{3.2}{3}\frac{t}{h}$$

This is the equivalent average flux linkage in maxwells per ampere uniformly distributed over the conductor section and for one inch length of a single conductor.

Thus the corresponding inductance in henry per inch length of conductor due only to the flux through the conductor is

$$L_c'' = \frac{3.2}{10^8}\left(\frac{t}{3h}\right) = \frac{1.067t}{10^8h}$$

and the total inductance per inch length

$$L'' = \frac{(1.6\delta-1.067t)}{10^8h} \text{ henry per inch length of each bar} \quad (14)$$

Although the mathematics of this calculation are very elementary and probably familiar to most of the readers, they are presented not only in order to emphasize the approximations involved in the simplified geometry, but also to point out the fact that apart from this assumption the resulting reactance is artificial and practically safe only when the thickness of the bar is considerably less than the skin depth at the operating frequency. At 60 cycles and for the proportions in question this condition is satisfied. Hence the 60-cycle reactance may be calculated with fairly good accuracy using the value of inductance just determined. This reactance is

$$X'_{ac\sim} = \frac{144.5}{10^6}\left[\frac{\delta}{h}+\frac{2}{3}\frac{t}{h}\right] \text{ ohms per loop foot} \quad (15)$$

the resistance is

$$r_f' = \frac{18.9\rho''}{10^6ht} \text{ ohms per loop foot} \quad (16)$$

$$\frac{X_{ac\sim}}{r_f} = \frac{7.65}{\rho''}\left[\delta t+\frac{2}{3}t^2\right] \quad (17)$$

If t were doubled, this important ratio would be nearly four times as great.

Appendix IV. Temperature Rise of Rectangular Parallel-Bar Bus

A rigorous mathematical analysis of the problems of heat transfer is quite complicated and certainly would be out of place in this paper. However, as in the case of the reactance computations it is possible to make a simple, approximate solution which is quite satisfactory from the designer's standpoint, and which has been checked experimentally in a few cases.

The heat path from the copper to the surrounding atmosphere has a certain thermal resistance which may be divided into two parts. The first is that of the insulating covering $\mathcal{R}_t$ and the second that from the surface of this covering to the surrounding air, R_a. The thermal contact resistance between the copper and its insulating covering is small in comparison to the other two items and can be neglected. The effective area of the insulating cover per inch length is approximately equal to the height of the copper plus the thickness of the copper at each end, or the thermal resistance per inch length is

$$\mathcal{R}_t'' = \frac{R''t_t}{h+2t}$$

where R_t'' is the thermal resistivity of the insulating material

t_t is the thickness of the cover sheet in inches
h is the height of the copper in inches
t is the thickness of the copper in inches

In the steady state, the thermal current which flows (heat) is proportional to the thermal potential difference (temperature) and inversely proportional to the thermal resistance. That is,

$$\Delta\theta = P\mathcal{R}$$

is the temperature drop through the thermal resistance, $\mathcal{R}$. This equation holds for any consistent set of units. For our purposes it is convenient to express the rate and heat flow P in watts, the thermal potential difference in degrees centigrade, and the thermal resistance $\mathcal{R}$ in thermal ohms (degrees centigrade per watt); hence the temperature drop in the insulation is

$$\Delta\theta_t = \frac{P''R_t''t_t}{h+2t}$$

A great many data have been collected by various investigators on the rate of heat dissipation from surfaces in still air. These investigations seem to indicate that the dissipation from bare copper is about 0.005 watts per square inch per degree centigrade. From a black surface such as we are concerned with here the dissipation is about 0.0075 watt per square inch per degree centigrade. The equivalent thermal resistivity R_a is therefore about 133 thermal ohms per square inch, for the black cover surface. Each side of the feeder must dissipate the heat generated in the conductor on that side, hence for one side:

$$\mathcal{R}_a'' = R_a''/(h+2t) \text{ thermal ohms per inch length of feeder}$$

The temperature rise in the copper is

$$\Delta\theta_c = \Delta\theta_a + \Delta\theta_t$$

The power to be dissipated per inch length is I_c^2r'' where I_c is the equivalent continuous current and r'' is the resistance per inch length of one conductor. In terms of feeder dimension and resistivity

$$r'' = \rho''/1.27th\times10^6$$

where ρ'' is the resistivity of copper in ohms per circular mil inch which is equal to 1 at 60 degrees centigrade.

Hence

$$\Delta\theta_c = \frac{I_c^2\rho''(R_a''+t_tR_t'')}{1.27th(h+2t)10^6} \text{ degrees centigrade} \quad (18)$$

In terms of current for a given temperature rise this becomes

$$I_c = \sqrt{\frac{1.27\,\Delta\theta_c th(h+2t)10^6}{\rho''(R_a''+t_tR_t'')}} \quad (19)$$

A convenient term of comparison is the circular mil per ampere which in this case is

$$\odot = \frac{1.27\times10^6ht}{I_c}$$

hence

$$\odot = \sqrt{\frac{1.27\rho''(R_a''+t_tR_t'')ht\times10^6}{\Delta\theta_c(h+2t)}} \quad (20)$$

The ratio of h to t normally would be about 20 or 30. It is apparent from the above equations that the larger this ratio the lower is the area required per ampere. In other words, for the range of dimensions in which we are interested the current carrying capacity of a feeder of a given section is approximately proportional to the square root of the height. Thus from the standpoint of both reactance and temperature rise it is advantageous to keep the ratio of height to thickness as large as possible. It is shown in Appendix V that the factor which limits this ratio to about 30 to 1 is the distance between clamps.

As an example of the foregoing calculations, find the equivalent continuous current which will cause a temperature rise of 40 degrees in the feeder whose cross section is shown in Figure 2. Suppose each conductor is $^3/_{16}$ inch by 4 inches, separated by $^1/_{16}$ inch of insulation and covered by $^1/_4$-inch sheets of black Micarta.

The resistivity of copper is about 0.676×10^{-6} ohm inches at 20 degrees centigrade, or in terms of ohms per circular mil inch at 60 degrees centigrade $\rho''=1$. For the type of surface with which we are here concerned the surface dissipation is about 0.0075 watt per square inch per degree centigrade, hence $R_a''=1/0.0075=133$ thermal ohms per square inch. The thermal resistivity of Micarta as supplied by the manufacturer is approximately 154 thermal ohm inches. The total effective thermal resistance therefore, is $R_a''+t_tR_t''=(133+38.5)=171.5$ thermal ohms per square inch. It is interesting to note that the contribution of the insulating material is only about 20 per cent of the total assuming the same surface dissipation factor for copper and the insulating material. Actually because of its bright surface, the effective R_a'' for copper is about 200 thermal ohms per square inch. Hence, the capacity of the bus actually is increased by the addition of the $^1/_4$ inch of black insulating material.

Substituting these values, I_c for this particular feeder is found to be, for a 40 degree centigrade rise, $I_c=990$ or $\odot=960$ circular mils per ampere. A test on this particular feeder resulted in a rise of 21.5 degrees centigrade when carrying 800 amperes at 31 degrees centigrade. Extrapolating and correcting for the change in resistivity a temperature rise of $(990/800)^2(294.5/265.5)(21.5)=37$ degrees centigrade when carrying the current calculated above which is a fair check on the assumed rise of 40 degrees centigrade.

Skin and proximity effects and eddy currents have been neglected in the above calculations. The shape and construction of the bus are such as to render these items unimportant. The measured a-c and d-c values of resistance differed only by 0.45 per cent and both of these values were within 2.5 per cent of the calculated resistance; 0.45 per cent is a reasonable check with the calculated value in which there was no allowance made for the contact resistance between joints.

Appendix V. Mechanical Forces Between Conductors of a Rectangular-Bar Feeder

The force in dynes on a conductor carrying a current in a magnetic field is the product of the flux density and the current in absolute units and the length of the conductor in centimeters.

Converting to practical units, the force in pounds is

$$F = Bli/11.3 \times 10^8$$

where

B is the flux density maxwells per square inch
l is the conductor length in inches
i is the current in the conductor in amperes

The flux density for conductors of the proportions and spacing with which we are here concerned is approximately

$$B = 3.2i/h$$

where h is the height of each bar in inches.

The force in pounds on each inch of the bar is therefore

$$F'' = 0.283i^2/h \times 10^8$$

If I_{ss} is the rms value of the worst short-circuit current the bus is likely to encounter and l_c is the distance in inches between clamps, the maximum force each clamp will experience, corresponding to the peak current is

$$F_{max} = 0.566 l_c (I_{ss})^2/h \times 10^8$$

If each conductor is considered as a fixed end beam, the maximum deflection at the center of the span corresponding to a total load of W pounds, uniformly distributed is

$$d_{max} = \frac{Wl_c^3}{384EM}$$

where

M is the moment of inertia of the section in inches^4
E is the modulus of elasticity

The inertia of the parts is such that the maximum deflection will not correspond to the peak force, but probably will be only slightly more than that due to the average value of the force; therefore d_{max} will be calculated on that basis.

$$W = l_c F'' = 0.283 l_c I^2_{ss}/h \times 10^8$$

For the section to be used

$$M = ht^3/12$$

For copper E is 16×10^6.

Substituting:

$$d_{max} = \frac{5.54 l_c^4 I_{ss}^2}{h^2 t^3 10^{16}}$$

or the distance between clamps in terms of the maximum allowable deflection is

$$l_c = \frac{0.652 d_{max}^{1/4} h^{1/2} t^{3/4}}{I_{ss}^{1/2}} \times 10^4 \tag{21}$$

Table III

s	s^2	$(n-s)$	$(n-s)!$	$s!$	d^s	$(1-d)^{n-s}$	$\frac{s^2 d^s (1-d)^{n-s}}{(n-s)!s!}$
0	0	15	1.31×10^{12}	1	1	0.269	0
1	1	14	8.74×10^{10}	1	$8.34/10^2$	0.294	0.280×10^{13}
2	4	13	6.23×10^9	2	$6.95/10^3$	0.321	0.716×10^{13}
3	9	12	4.79×10^8	6	$5.79/10^4$	0.350	0.634×10^{13}
4	16	11	3.99×10^7	24	$4.83/10^5$	0.382	0.308×10^{13}
5	25	10	3.62×10^6	1.2×10^2	$4.03/10^6$	0.418	0.0976×10^{13}
6	36	9	3.62×10^5	7.2×10^2	$3.46/10^7$	0.456	0.0218×10^{13}
7	49	8	4.03×10^4	5.04×10^3	$2.80/10^8$	0.496	0.0033×10^{13}
8	64	7	5.04×10^3	4.03×10^4	$2.33/10^9$	0.543	0.0004×10^{13}
9	81	6	7.2×10^2	3.62×10^5	$1.94/10^{10}$	0.592	Negligible
10	100	5	1.2×10^2	3.62×10^6	$1.62/10^{11}$	0.641	Negligible
11	121	4	24	3.99×10^7	$1.35/10^{12}$	0.705	Negligible
12	144	3	6	4.79×10^8	$1.12/10^{13}$	0.769	Negligible
13	169	2	2	6.23×10^9	$0.940/10^{14}$	0.840	Negligible
14	196	1	1	8.74×10^{10}	$0.782/10^{15}$	0.9166	Negligible
15	225	0	1	1.31×10^{12}	$0.650/10^{16}$	1	Negligible
							$\Sigma = 2.060 \times 10^{-13}$

Appendix VI

Example of Feeder Design

Given the following information design a feeder 300-feet long to supply welding power at 440 volts.

Number of machines on the feeder—$n = 15$
Average current per machine—$I_a = 410$ amperes at 440 volts
Average duration of each weld—$a = 10$ cycles
Probable maximum rate of welding—30 welds per minute per machine
Length of feeder—$l = 300$ feet
Weld period = 3,600 ÷ welds per minute—$a + b = 120$ cycles
Off time—$b = 110$ cycles
Duty cycle—$d = 10/120 = 0.0833$

The structures being welded are of such nature that one understrength weld in 5,000 will be acceptable (that is, $Q = 5{,}000$).

Find the number of machines the feeder must be designed to accommodate simultaneously, if no more than one weld in 5,000 is to be understrength; that is, find $s = s_0 + 2$ such that:

$$P_{\geq(s_0+2)} = 1/5{,}000$$

and design the feeder to accommodate the current of s_0 machines with a feeder voltage drop of not more than five per cent. It has been shown that

$$P_{\geq s} = \frac{1}{(a+b)^{n-1}} \sum_{h=0}^{n-1-s'} (2a-2k+1)^{n-1-h} \times (b-a+2k-1)^h \frac{(n-1)!}{(n-1-h)!h!}$$

$$(a+b)^{n-1} = (a+b)^{14} = 12.8 \times 10^{28}$$

$0.25a = 2{,}5k = 2$. Note $(0)! = 1$

Values for the terms of the equation will be found in Table II.

From column L it can be seen that a coincidence of nine machines for at least two cycles is likely to occur once in 5,900 welds. That is, when

$$s = 9, \quad P_{\geq s} \gtreqless 1/5{,}000$$
$$\therefore s_0 + 2 = 9 \quad \text{or} \quad s_0 = 7$$

Therefore if the feeder be designed to carry the current of seven machines with a feeder drop of five per cent, eight machines will produce a differential feeder drop of five per cent. Nine machines will cause a differential feeder drop of more than five per cent, hence understrength welds will result if nine or more machines coincide. According to our probability calculations this is not likely to occur more often than once in 5,900 welds, which is acceptable according to the statement of the problem. (Note that one might do well to take advantage of the fact that by designing for one more machine the understrength weld ratio could be reduced to 1/100,000.)

$$s_0 = 7 \qquad I_a = 410 \qquad I_s = s_0 I_a = 2{,}870$$

The maximum impedance drop is to be five per cent. Hence,

$$I_s Z_f = (0.05)(440) = 22 \text{ volts}$$

The impedance of the feeder must be

$$Z_f \gtreqless 22/2{,}870 = 0.00768$$

The reactance contributes so little to the impedance that in order to get the approximate size of the conductors it almost can be neglected. Therefore, assume that the resistance is approximately 0.007 ohm.

The feeder resistance is

$$r_f = 24\rho'' l/1.27th \times 10^6$$

where

ρ'' is the resistivity in ohms per circular mil inch = 1 at 60 degrees centigrade

l is the feeder length (one-half the conductor length) in feet
t and h are the dimensions of one conductor in inches.

The area required for each conductor is

$$th=24\rho''l/1.27r_f\times10^6$$

which in this case is $th=0.81$ square inch; h/t should be at least 20. If conductors $^3/_{16}$ inch by 4.25 inches are chosen the feeder resistance will be

$$r_f=7.12\times10^{-5}\text{ ohms}$$

If the conductors are spaced $^1/_{16}$ inch apart the reactance at 60 cycles will be from equation 3:

$$X'=6.38\times10^{-8}\text{ ohms per loop foot}$$

The total reactance is

$$X_f=1.914\times10^{-5}\text{ ohms}$$
$$Z_f=(7.12+j1.91)\times10^{-5}\text{ ohms}$$
$$|Z|=7.39\times10^{-5}\text{ ohms}$$

This arrangement is obviously satisfactory from an impedance standpoint.

TEMPERATURE RISE

The most probable equivalent continuous current is, from equation 4.

$$I_e=I_a\sqrt{n!\sum_{s=0}^{n}\frac{s^2d^s(1-d)^{n-s}}{(n-s)!s!}}$$

Values for the terms of the equation will be found in Table III.

$I_e^2=I_a^2n!\Sigma=(410)^2(1.31)(2.060)$
$I_e=674$ amperes is the most probable equivalent continuous current
$I_{rms\,max}=n\sqrt{d}\ I_a=1{,}780$ amperes is the maximum possible equivalent continuous current

The temperature rise in degrees centigrade is given in equation 18 as

$$\Delta\theta_c=\frac{I_c^2\rho''(t_iR_i''+R_a'')}{1.27ht(h+2t)10^6}$$

at 60 degrees centigrade $\rho''=1.0$.

If the feeder is constructed similar to that shown in Figure 2, $R''=154$ thermal ohm inches. For this type of material $K=0.0075$, $R_a=1/0.0075=133$, $t_i={}^1/_4$ inch.

I_c has just been shown to be 674 amperes.

Hence, $\Delta\theta_c=16.6$ degrees centigrade is the temperature rise which is well within safe limits.

Mechanical Construction

The maximum probable short-circuit current depends so much on factors other than the feeder that no attempt will be made here to justify the figure presented. In the few installations the authors have encountered, an rms value of short-circuit current of 20,000 amperes is a reasonable figure. A deflection under short circuit of $^1/_8$ inch would not be excessive. Actually because of the support of the insulating cover, the deflection will be less than that calculated by these formulas. Equation 21 gives the distance between clamps as $l_c=16.5$ inches.

The supporting hangers are not subject to the forces between conductors, hence need not be spaced so closely.

The foregoing example is presented merely as an illustration of the application of the design technique explained in this paper. The resulting design will depend, of course, upon the number of machines, average current, average duty cycle, the length of the feeder, and the voltage.

Although the several formulas show the effects of these variables, it may be interesting to consider briefly the result of changing the distribution voltage from 440 to 220.

Other things being equal, the current will be double, the permissible voltage drop one half, and the impedance one quarter.[4] Or, since in this case the resistance is the dominant factor, the conductor section will be four times that indicated above or 3.3 square inches.

If the same ratio of width to thickness is maintained, the temperature rise will be even less than that of the 440-volt bus for the same duty and negligible from the practical point of view.

Assuming the same permissible deflection under short circuit, the same ratio of width to thickness, and double the short-circuit current, the distance between clamps will be greater by about 68 per cent.

Since this large increase in bus section would add greatly to the cost, it probably would be an economy to increase the number of substations and decrease the maximum feeder length as well as the number of machines on the longest feeder.

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Impulse Strength of Insulated-Power-Cable Circuits

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Synopsis: Pursuant to a request of the cable working group, AIEE committee on power transmission and distribution, a co-ordinated study has been made on the impulse strength of insulated power cables having various types of insulation and their accessories. The data included not only all published information, but also much privately contributed information. On the basis of these data and some supplementary tests recently made, tabulations of safe withstand impulse voltages have been prepared.

The information is also discussed with regard to its significance for practical applications from the standpoint of switching surges as well as from the standpoint of lightning impulses. Recommendations are given for standardizing impulse-test procedures.

THE importance of proper co-ordination of insulated-power-cable circuits with other equipment in regard to withstanding transient over voltages has received increasing attention in recent years. Recognizing the need for sound and complete technical information upon which the design of cable circuits can be based, the cable working group of the AIEE committee on power transmission and distribution asked the authors of this paper to analyze and co-ordinate all available information on this subject for the benefit of the industry. This information would in some cases be used in deciding the need for special lightning protection. It was recognized that in certain respects the information available is meager and that much work has yet to be done to place recommendations upon a definitely reliable basis. It was felt, however, that a useful purpose would be served by analyzing and co-ordinating all available data rather than wait for addi-

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The authors wish to express their gratefulness to the following organizations for making available previously unpublished data: The Detroit Edison Company, General Cable Corporation, General Electric Company, and The Okonite-Callender Cable Company. Special thanks are due to the Simplex Wire and Cable Company and the Boston Edison Company for contributing time and equipment to conduct tests on cable joints for the special purpose of aiding the authors in filling some of the gaps in the information. Acknowledgment is due H. A. Adler of the Commonwealth Edison Company for his great aid in preparing the paper.

tional data. In view of the great time and effort required to produce data on impulse characteristics of cables and their accessories and the limitations imposed on laboratories by the present war emergency, it may well be several years before much additional information would become available.

Many of the data presented have been previously published in separate articles as indicated in the list of references at the end of paper. In order to save space, much of the presentation of data and pertinent discussion is given in a brief way in the body of the paper. Nevertheless, it may be noted that a great deal of study was given to the detailed data in arriving at the findings shown in the paper.

The paper is divided into three sections. The first section presents an analysis of information on the impulse strength of cables and accessories. The second section gives information on the application of these data for the design of cable circuits. The third section makes recommendations on the standardization of methods of making impulse tests in order that the results of future tests will be more exactly comparable than some of the present data.

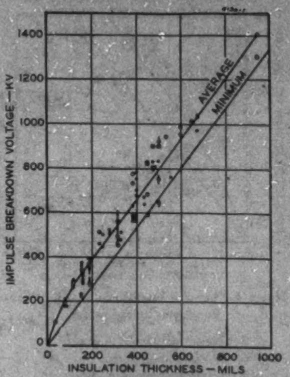

Figure 1. Impulse breakdown voltage versus insulation thickness of impregnated-paper-insulated single-conductor cables

Data represent results from ten different laboratories

●—Solid
○—Oil filled
□—Compression
△—Oilostatic

Section I. The Impulse Strength of Cables and Accessories

Impregnated-Paper-Insulated Cables

Information on the impulse strength of impregnated-paper-insulated cables seems fairly adequate.[1-14] Figure 1 shows the available data for single-conductor cables of this type. In many cases in this and other figures the points shown are each based upon a number of tests. In such cases the points in the graphs are the average values of the results, and wherever possible the spread of the individual results is indicated by vertical lines. Most of the data were obtained on samples of new cable.

Considering that the data in Figure 1 represent the results obtained by ten different laboratories and that there were important differences in test methods, the scattering of the results is remarkably small. The line for average breakdown voltages indicated in Figure 1 can be used, therefore, with some confidence. It can be seen that the available data show little if any difference between the breakdown strength of solid, oil-filled, Oilostatic, and compression cables.

Based on the slope of the average curve, a line has been drawn through the minimum results. The values represented by this line are considered the minimum impulse voltage which cables of this type can be expected to withstand. On this basis the minimum impulse strength is 1,330 volts per mil independent of insulation thickness. A value ten per cent lower, or 1,200 volts per mil, is recommended for use as safe withstand impulse strength. The resulting withstand voltages for solid and oil-filled cables of various voltage classes are shown in Table I. The withstand voltages for solid cables are much higher than the withstand test voltage, that is basic impulse insulation levels, established for other apparatus,[29] while the withstand voltages for oil-filled cables are only slightly above the basic impulse insulation levels.

In some cases, especially for small radii of curvature of the conductor, maximum rather than average stresses may be of interest. In Figure 2 the results are replotted as a function of the factor $r \log_e R/r$. The voltage divided by this factor gives the stress in the insulation adjacent to the conductor. On the basis of this graph the average impulse stress of the insulation immediately adjacent to the conductor is 2,400 volts per mil. Some available data indicate that for cables with equal insulation thicknesses the one with the smaller conductor has the lower breakdown voltage. Other data appear to contradict this. Likewise, for cables having the same stress at the conductor for a given voltage the one with the heavier insulation has the higher impulse breakdown voltage. The available data are, however, not always consistent in this respect and it must be assumed that in most cases other variables overshadow the differences in the effects of maximum and average stresses. It, accordingly, is not possible to establish a definite relationship between the two at the present time.

Maximum stresses may become important in the case of three-conductor cables with sector-shaped conductors. Figure 3 shows test results for three-conductor sector shielded cables of the solid type. The values fall mostly below the average line for single-conductor cables when plotted versus insulation thickness, while on the basis of maximum stresses the results are well in line with those for single-conductor cables. Test failures have almost always occurred at the corners of the sectors, which is the location of maximum stress. However, the minimum curve of breakdown voltage versus insulation thickness for single-conductor cables applies equally well for three-conductor sector shielded cables with the result that the recommended withstand stress of 1,200 volts per mil can be accepted also for sector-conductor cables.

Figure 3 also shows some results for used cables with inferior impregnation which indicate a lower impulse strength for such insulation compared with well-

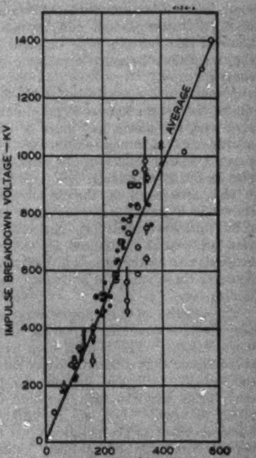

Figure 2. Impulse breakdown voltage versus $r \log R/r$ for impregnated-paper-insulated single-conductor cables

Data represent results from ten different laboratories

r = radius of bare conductor in mils
R = radius of insulated conductor in mils

●—Solid
○—Oil filled
□—Compression
△—Oilostatic

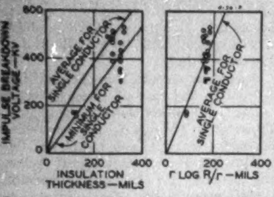

Figure 3. Impulse breakdown of solid three-conductor shielded cables

O—Impregnation good
●—Impregnation fair to poor

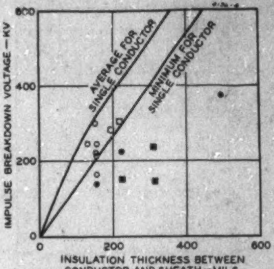

Figure 4. Impulse breakdown of solid three-conductor belted cable

O—New, impregnation good
●—Nonmigrating compound
■—Used, impregnation poor
□—Used, impregnation unknown

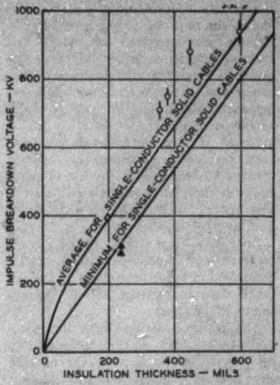

Figure 5. Impulse-breakdown voltage for gas-filled cables compared with solid cables

△—Low-pressure single conductor
▲—Low-pressure three-conductor, shielded
O—High pressure

impregnated insulation. Similar conclusions are reached from Figure 4 for three-conductor belted cables of the solid type. Poorly impregnated insulation appears to have decreased impulse strength. In general, the data available show little or no effect on the impulse strength of the insulation due to service or test aging, except for special cases.

The results for new well-impregnated three-conductor belted cables fall below the average line for single-conductor cables but sufficiently within the minimum line so that a safe withstand voltage of 1,200 volts per mil can be recommended also for this type of cable for the insulation between conductor and sheath.

A few data are available for gas-filled cables of both the low- and high-pressure type and are shown in Figure 5. The average and minimum lines for other paper-insulated cables apply quite well also for this type of cable.

The agreement of the test results shown in Figure 1 was considered very good from several angles, especially in view of several differences in the test methods used by the various investigators. Some of these variables are worth mentioning here.

The polarity of the impulse wave appears to have some effect upon the breakdown voltage. The limited data available indicate that solid-type paper cables can usually withstand five or ten per cent higher positive than negative impulse voltages,[7,11] but one investigator found no effect of the polarity upon the impulse strength of oil-filled cables.[7] The effect of polarity is of practical interest because the majority of lightning strokes have been found to be of negative polarity. The points in Figure 1 represent results partly for positive and partly for negative polarity. It appears that the effects of polarity were overshadowed by the effects of other variables. The withstand stress of 1,200 volts per mil derived from Figure 1 can be considered to apply to either polarity.

The wave shapes varied over a wide range in these investigations. The lengths of the wave fronts ranged from 0.5 to 2.5 microseconds and the times to half value of the tails of the waves ranged from 7 to 500 microseconds. The available data seem to indicate that within the limits of the investigations the wave shapes appear to be of minor importance as compared with the effect upon the impulse strength of other variables.

Voltage application was in steps in most of these investigations, that is, the impulse voltages were increased in relatively small steps from a low value until breakdown occurred. Most breakdowns in these tests probably occurred at the crest or tail of the wave. Some investigators used single-shot tests which probably resulted in some cases in breakdowns on the front of the wave.

One hundred to 200 repeated impulses at voltages at five or ten per cent below the critical breakdown value have little if any permanently damaging effect upon impregnated-paper insulation according to the findings of several investigators. In other words, there is no indication of an impulse fatigue for the number of severe surges that would be expected to occur in service.

It is of interest to note that the voltage–time characteristics of class *A* materials such as oil-impregnated linen and oil-impregnated fullerboard have been investigated by two different laboratories.[15,16,17] For impregnated fullerboard, the breakdown voltage decreases with increasing time to breakdown up to about one or two microseconds. The breakdown voltage then remains constant up to about 0.1 second, when it starts to decrease again with increasing time. Meager data available indicate that the voltage–time relation for impregnated-paper-insulated cables follows similar characteristics. The time-lag curves in Figure 6 obtained by two investigators[7,11] show that the impulse strength of impregnated-paper-insulated cables decreases up to durations of one or two microseconds and then remains practically constant up to at least 20 microseconds at about the breakdown stress derived from Figure 1. In Figure 7 the results of impulse tests are compared with five-minute and six-hour 60-cycle test results. These data are the results of the standard high-voltage and high-voltage–time tests evaluated by the sixth-power law. Although the impulse and 60-cycle tests were not made on the same cable, they were made on cables of the same type made in the same year by the same manufacturer. As indicated by the dashed line, these data appear to fit a characteristic similar to the one found for impregnated fullerboard.

Lightning surges are of very short durations and the strength of cables under lightning impulses is, therefore, determinable by means of the test results shown in Figure 1. Switching overvoltages are, according to several investigators, in general of much longer duration,[18–26] that is, the durations of the higher portions of the transient overvoltages may be up to about 2,000 microseconds and in some cases may even be up to one or two cycles on a 60-cycle basis. The strength of cables under switching surges can thus be considered to fall into the unexplored region of the voltage-time curve between impulses of short duration and 60 cycle tests.

It is believed, on the basis of the meager information available, that the ability of impregnated-paper insulation to with-

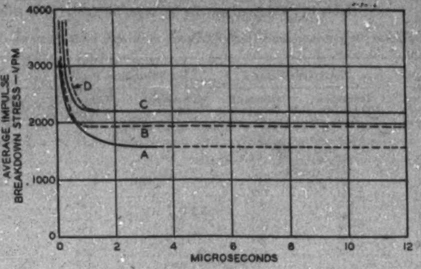

Figure 6. Impulse-breakdown–time-lag curves for impregnated-paper-insulated cables

Curve *A* obtained in one laboratory and curves *B*, *C*, and *D* obtained in another

A—325 mils of solid insulation, 1×50-microsecond wave, average of values with positive and negative polarities

B—236 mils of solid insulation, $2^1/_2\times50$-microsecond wave, negative polarity

C—236 mils of oil-filled insulation, $2^1/_2\times50$-microsecond wave, positive and negative polarities

D—236 mils of solid insulation, $2^1/_2\times50$-microsecond wave, positive polarity

stand a switching surge is little if any below the withstand stress of 1,200 volts per mil for impulses of short duration. However, one investigator[18] concluded that the effect of switching surges is cumulative and that during the normal life of a cable its ability to withstand switching surges is equivalent to the five-minute breakdown strength. More work on this problem is desirable. For the present, it appears that a withstand value of 1,000 volts per mil crest for switching surges is a reasonably conservative figure.

Varnished-Cambric-Insulated Cables

The data on the impulse strength of varnished cambric cables are comparatively meager. The available information is shown in Figure 8. On account of lack of sufficient data for cable alone, results for varnished cambric in sheet form have been included which appear to agree quite well with the data for cable. Some results for taped varnished cambric joints are also shown.

In contrast to impregnated-paper insulation, varnished-cambric insulation shows a considerable decrease in unit impulse strength with increasing insulation thickness. As for paper-insulated cables, a curve has been drawn through the minimum values of impulse breakdown voltages. From this curve, values of minimum breakdown stress and safe withstand stresses ten per cent lower have been derived. All this information is shown in Figure 8. The withstand voltages derived from this figure are shown in Table I, and the values are substantially above the basic insulation levels for equipment.[30]

In contrast with impregnated-paper insulation, varnished cambric shows some fatigue under repeated impulses.[11] Studies of test data indicate for varnished-cambric insulation that for the number of high impulses to be expected in service the breakdown strength is five or ten per cent less than the breakdown strength obtained with a few impulses. For at least this reason, in contrast with paper-insulated cables that usually suffer practically no loss of impulse withstand strength due to service, varnished-cambric-insulated cables should be considered as having a slight reduction in withstand strength in service below the withstand strength for new cable given in Table I.

Rubber-Insulated Cables

The available data indicate differences in the impulse strength of various grades of rubber. The results for ordinary grades, such as Code and 30 per cent compounds, are shown in the lower portion of Figure 9. Average and minimum curves are indicated. As for varnished-cambric insulation, the impulse breakdown stress decreases with increasing insulation thickness, but to an even greater extent. The withstand voltages based upon these data are shown in the last column of Table I. For rated voltages of five kv and less, these withstand voltages are higher than the basic impulse insulation levels,[29] while above the five-kv class the reverse relation applies.

Some comparative test results recently obtained by the General Electric Company on cables indicate that there is little or no difference in the impulse strength of 30 per cent performance insulations compounded with natural rubber or with Buna S. The test results averaged 127 kv for about 260 mils of insulation. Data furnished by another manufacturer indicate that performance, ozone resistant and 60 per cent compounds made with synthetic rubber have a somewhat lower impulse strength than the corresponding natural-rubber compounds.

The middle portion of Figure 9 shows results for ozone-resistant rubber compounds. According to the available data, the average impulse strength of this type of rubber is higher than the average strength of ordinary grades of rubber. However, in view of some of the low results shown for ozone-resistant rubber in Figure 9, and on account of lack of sufficient information, it is not felt advisable to give definite recommendations at this time for higher withstand stresses for ozone-resistant rubber compounds than for ordinary grades. It is believed that the margin of safety with regard to the withstand voltages in Table I is about 30 per cent higher for ozone-resistant rubber than for ordinary grades, and that more definite recommendations cannot be made for withstand voltages until more test data with heavy walls of insulation are available.

Still higher impulse strength is indicated for 60 per cent rubber compounds by the data in the upper portion of Figure 9. Since these data are meager and represent test results of only one laboratory, it is not considered advisable to attempt to establish withstand voltage for such compounds at the present time.

There is indication that, other things being equal, the maximum stress may have an effect to some extent upon the breakdown voltage because for some samples with equal insulation thicknesses but different conductor sizes the impulse-breakdown voltages were higher for the larger than for the smaller conductors. As with paper- and varnished-cambric-insulated cables, however, the results are too contradictory to draw definite conclusions at the present time.

Prolonged immersion in water of rubber-insulated cable with nonmetallic covering may produce considerable reduction in impulse strength. This conclusion is based on data obtained in one set of tests of 30 days in tap water, alkaline water, and acid water.

Joints

Information on the impulse strength of joints is meager. However, some comparative data are available to give a general idea on the relative strength of joints and cables. The information obtained in the special tests of the Simplex Wire and Cable Company and the Boston Edison Company, which is summarized in Table II, was especially helpful.

The results shown in Figure 8 for varnished-cambric-taped joints on solid-type cables indicate that the information on the impulse strength of varnished-cambric cables can be used with some assurance for determining the probable impulse strength of varnished-cambric joints. The values for joints in Table III have been derived from the minimum breakdown voltage curve in Figure 8 extrapolated for heavier insulation thicknesses, using joint designs of two

Table I. Withstand Impulse Voltages for Cables With Metallic Covering

Values Are Based Upon Present Insulation Thicknesses and Available Information on Corresponding Unit-Stress Impulse Strengths

Reference Class, Kv	Basic Impulse Insulation Level for Equipment	Solid-Type Paper		Oil-Filled-Type Paper		Varnished Cambric		Ordinary Grades of Rubber	
		Insulation Thickness, Mils	Withstand Voltage, Kv	Insulation Thickness, Mils	Withstand Voltage, Kv	Insulation Thickness, Mils	Withstand Voltage, Kv	Insulation Thickness, Mils	Withstand Voltage, Kv
1.2	30	78	94			78	67	78	50
2.5	45	78	94			94	79	109	58
5.0	60	94	113			125	101	156	64
8.7	75	141	169			188	142	203	68
15	110	203	244	110	132	250	180	297	73
23	150	266	319	145	174	375	248		
34.5	200	375	450	190	228				
46	250	469	563	225	270				
69	350	688	825	315	378				
115	550			480	575				
138	650			560	672				
161	750			648	780				
230	1,050			925	1,110				

utilities. From Table III it appears that for 15 kv joints the withstand impulse strength is about the same as for the cable but that the relation decreases for the higher-voltage classes becoming only 65 per cent for 69-kv joints. For 24-kv joints the impulse withstand voltage is about 94 per cent of that of 24-kv cables. It should also be kept in mind that the withstand voltages given in Table III provide more margin of safety for cables than for joints, because the ratio of average to minimum strength for solid-type cable is larger than for varnished cambric insulation. Figure 10 shows the relative strength of solid-type cables and accessories in a graphical form and gives also actual test data for joints.

Some information on safe longitudinal stresses in joints is available from records on five longitudinal breakdowns along the steps of joints in impulse tests. Two of these breakdowns occurred in experimental 15-kv single-conductor varnished-cambric joints, two in a 24-kv three-conductor shielded varnished-cambric joint, and one in a 24-kv three-conductor shielded paper-reinforced duct splice. The average longitudinal stresses at breakdown were about 70 volts per mil for leakage distances of 2.3 to 3.75 inches (see Table III) and were about 50 volts per mil for 7.4 to 8.6 inches. Impulse tests of a setup of oil-filled 162-kv cable which included a normal joint having a leakage distance of about 21 inches resulted in failure elsewhere than in the joint at 1,010 kv and produced a partial radial failure in the joint. All of these data indicate that for moderate or long longitudinal paths in joints, the impulse breakdown strength is about 50 volts per mil.

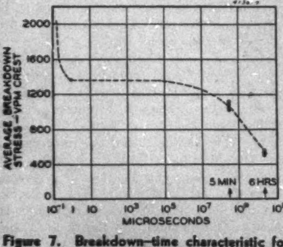

Figure 7. Breakdown–time characteristic for one lot of 500,000-circular-mil three-conducto shielded nine-kilovolt cable of the solid type made in 1941

For oil-filled cables (they have much thinner insulation than do solid-type cables) the available data indicate that cables and joints are of about the same impulse strength. Some of the data are in Figure 11; other data are deduced from recorded lightning surges that entered experimental cable connected to a 132-kv overhead line in Chicago and from unpublished short-time 60-cycle test results obtained on 69-kv cable and joints. The same withstand voltages apply, therefore, for cables and joints.

Varnished-cambric-insulated joints on varnished-cambric cables can be assumed to be at least as strong as the cable. This statement is based on the fact that according to Figure 8 the breakdown stress of hand-applied varnished-cambric insulation is about the same as that of factory insulation and that the insulation thickness of joints is greater than that of cables of the same rating.

Rubber-insulated joints of standard design are probably stronger in general than rubber cables. This conclusion is based on the results of impulse tests of rubber-insulated cables containing joints shown in Table II.

Outdoor Terminals

Typical impulse-flashover data for outdoor cable terminals are shown in Figures 10 and 11. Withstand voltages cannot be given. However, an AIEE committee is studying this problem at the present time. Obviously for solid-type-cable circuits, the impulse strength of terminals of a given voltage class is less than for the cable or joints, while cable and accessories have approximately the same strength for oil-filled circuits.

Section II. Transient Overvoltages in Cables

The primary purpose of this paper is to co-ordinate all available information on the impulse strength of insulated power cables and accessories and, as far as present data will allow, establish representative values. From this analysis it is possible to recommend the transient-voltage stresses cables can be subjected to in service with reasonable safety. All of this has been covered in section I.

A detailed analysis of the methods of estimating the transient voltages to which power systems as a whole, and cables especially, are exposed in service is not within the scope of this paper. The transient-voltage problem has been and

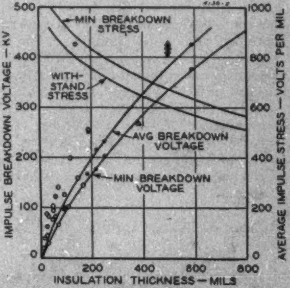

Figure 8. Impulse breakdown of varnished-cambric insulation

●—Cables
○—Sheets
△—Joints

Table II. Impulse Tests of Joints on Single-Conductor Cable

Cable Data				Joint Data			Breakdown			Stresses at Breakdown, Volts Per Mil		
Rated Voltage, Kv	Conductor Size	Insulation		Type of Insulation	Radial Thickness, Mils	Longitudinal Leakage Path, In.	Location	Type	Voltage, Kv	In Cable	In Joint	
		Type	Mils								Radial	Longitudinal
15	350,000 cir mils	Solid	175	Varnished cambric	385	2.3	Joint	Longitudinal	160	(915)	(415)	69.5
					383	2.875	Joint	Longitudinal	225	(1,285)	(587)	78.2
					378	3.75	Joint	Radial	270	(1,543)	715	(72)
5	4/0 American wire gauge	Ozone-resistant rubber	125	Ozone-resistant rubber	195	1.125	Cable ♯		81	648	(415)	(72)
						1.75	Cable at terminal		67.5	540	(346)	(38.5)
						2.25	Cable at bend		88	784	(451)	(39)
						0.75	Cable		102	816	(523)	(136)
5	4/0 American wire gauge	Ozone-resistant rubber*	125	Ozone-resistant rubber	195	0.75	Cable		99	792	(507)	(133)

* Without use of rubber cement; all other rubber joints were built with rubber cement.

♯ Failure not definitely located.

Stresses at various points are shown. Values in parentheses are for points not involved in the failure.

is still under active study by various coordinating committees and a number of individuals. A great deal of this work has been published and the papers, articles, and reports in the reference list[15–30] should be consulted.

A brief summary of the transient voltages as related to cable circuits will be outlined for the guidance of those interested, but it should be understood that it will be necessary to refer to the authorities cited for working details.

Lightning Surges

In many cases cable circuits with grounded metallic coverings are self-protective because of relatively very low surge impedance that causes a sharp reduction in the crest of the incoming wave and because of adequate length of circuit and high impulse strength. Induced lightning surges may become a serious hazard only in few cases, mostly involving either cables of the lower voltage ratings or relatively short lengths of circuit of any voltage rating, connected directly to overhead lines. In general, careful attention is required for the cases having short lengths of cable connected to overhead lines, especially for connections to overinsulated lines of high impulse strength, such as wood-pole lines with ungrounded hardware. While no general rules can be laid down, such cases are especially apt to require the use of protective devices, and in all cases of cables connected to overhead lines the need for the installation of lightning arresters or protector gaps should be investigated.

Direct connection of cable circuits to overhead lines requires more careful attention to lightning protection at the lower voltage ratings than the higher. The reason for this is that the ratio of the impulse strength of overhead lines to that of cable circuits increases materially with decrease in voltage rating, even when wood-pole construction with ungrounded hardware is not involved.

Another condition that requires special attention is the connection of nonmetallic unshielded rubber-insulated cable to overhead lines. The relatively low impulse strength of rubber-insulated cable is dealt with in section I. The lack of a grounded covering materially reduces capacitance and this, in turn, means an increase in the lightning voltage the cable will be subjected to. High-impedance metallic coverings, such as open-spiralled wire and ribbon wrappings without lead sheath, also tend to increase lightning surge voltages in cables.

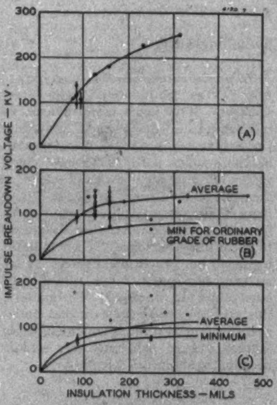

Figure 9. Impulse breakdown voltages of rubber-insulated cables

A—Data from one laboratory; 60 per cent rubber

B—Data from four laboratories; ozone resistant rubber

C—Data from three laboratories; ordinary grades of rubber

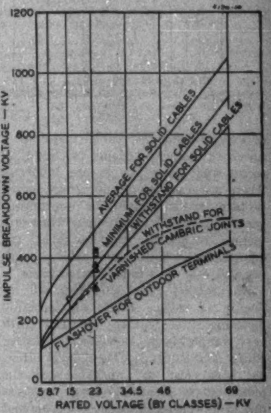

Figure 10. Impulse strength of solid cables and accessories

It is frequent practice to select terminals of the next higher voltage rating than the cable

O—Impulse breakdown of varnished-cambric joints

Δ—Impulse breakdown of paper duct splices

In general, the covering of a metal-covered cable should be grounded at the junction of the overhead and underground portions of a circuit. In case a protective device is used, it is important that the ground side of the device is connected to the metallic covering through a short metallic connection with any auxiliary ground attached thereto.

While in many cases cable circuits are self-protective this does not necessarily mean that this protection is imparted to connected equipment. The shape and magnitude of the incoming lightning-voltage wave, length of cable circuit, ratio

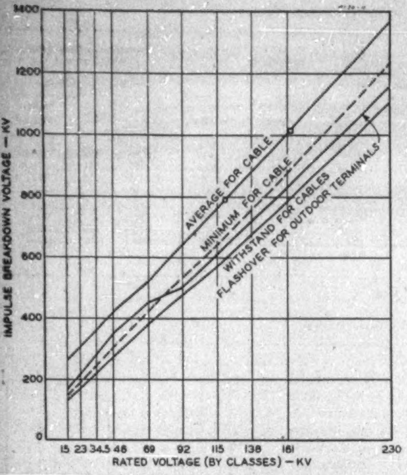

Figure 11. Impulse strength of oil-filled cables and accessories

□—Special joint on oil-filled cable
○—Condenser joint on gas-filled cable

of overhead-line and cable surge impedances, the surge impedance of the equipment, and other factors are involved. There are so many ramifications to the problem that it is impossible to formulate general rules that will indicate when protection is needed for the cable, the equipment, or both. It is safer to work out each case as an individual problem.

The factors that determine the influence of cable circuits on incoming lightning surge voltages are dealt with very completely in the paper by Brinton, Buller, and Rudge.[19] Their paper brings out in a clear and workable way the reduction in lightning surge voltages due to low surge impedance of cable and the importance of sufficient cable length to accomplish this. End reflections in shorter lengths materially increase the surge voltage increasing the need for protective devices.

Those factors affecting the magnitude of the incoming waves from overhead lines are too involved for a single paper. The list of references given should be of help and three of these papers,[27,28,30] in particular give useful information on these characteristics, including impulse strength data on line insulators, bushings, co-ordinating gaps, and equipment.

After the maximum lightning voltage to which the cable circuit will be subjected is once established, it is necessary to make sure that the recommended withstand voltage of the cable and accessories equals or exceeds this. For convenience, the recommended withstand impulse stresses for different types of cable and joint insulations dealt with in section I are summarized in Table IV.

Switching Surges

Switching surges are the most important of the self-induced system overvoltages. It is much more difficult to recommend safe withstand switching stresses for cables than is the case with lightning surges, partly because switching surges fall into the unexplored region of the voltage–time curve in Figure 7 and there is only meager indirect test data to be used as a guide. Even this is limited to paper-insulated cable. There is nothing at all but practical experience as far as varnished-cambric and rubber-insulated cables are concerned. Fortunately, this experience does indicate that under average normal operating conditions cable failures due to switching very seldom occur in the absence of defects.

Special cases do require attention, however. One of these is switching of arc furnaces. There are indications that under such conditions switching surges can and do exceed the usual maximum values of three to five times normal crest voltage and cable failures have occurred from this cause. Another special case is high-side switching of long lines of extra-high-voltage cable. There has been little field experience and no reported cable service failures, but test and theoretical considerations do indicate that excessive overvoltages can be experienced, due to high charging current and restriking of the circuit-breaker arc.

As stated in section I, switching surges are of much longer duration than lightning surges. Indirect evidence, such as that given in Figure 7, indicates that paper-insulated cable can withstand a single switching surge only a little, if any, lower than the recommended withstand impulse voltage stress of 1,200 volts per mil. It is probable, however, that repeated switching surges have a more pronounced cumulative effect than lightning surges because of longer duration. To take care of this, a switching-surge withstand stress of 1,000 volts per mil is suggested as conservative until more is known about it. This is 83 per cent of the recommended impulse withstand stress.

Table IV. Recommended Withstand Impulse Voltage Stresses for Cable and Accessory Insulations

Insulation Thickness, Mils	Recommended Withstand Impulse Stresses, Average Volts Per Mil		
	All Types Paper Insulation	Varnished-Cambric Insulation	Ordinary Grades of Rubber*
75	1,200	866	625
100	1,200	840	540
150	1,200	792	420
200	1,200	754	338
250	1,200	724	283
300	1,200	696	245
350	1,200	670	215
400	1,200	646	193
500	1,200	602	
600	1,200	566	
700	1,200	536	
800	1,200		
900	1,200		
1,000	1,200		

* Tentative recommendations for ozone-resistant rubber are 30 per cent higher than the values given in this column.

In view of the fact that varnished-cambric and rubber insulations show a cumulative effect even from repeated impulses of short duration, it is doubtful that this same reduction would safely take care of switching surges. Probably a reduction to 75 or 80 per cent of the impulse withstand stresses given in Table IV for varnished cambric, and 70 to 75 per cent for rubber, would be better, but this, of course, is nothing more than

Table III. Withstand Impulse Voltages for Varnished-Cambric Joints on Solid-Type Cables

Rated Kv	Joint Insulation, Mils	Min Voltage for Joint, Kv	Withstand Voltage for Joint, Kv	Withstand Voltage for Solid-Type Cable, Kv
15	383	275	247	244
24	500	335	301	319
34.5	805	460	414	450
69	1,160	580	522	825

judgment, based on theoretical speculation, until direct-test evidence is available.

The problem of switching surges in power systems is being actively studied by a number of engineers.[10–18] With the help of transient-voltage analyzers, augmented by field studies, it appears to be only a question of time before the magnitude, wave shape, and duration of switching surges can be reliably predetermined for all of the various conditions met with in power systems. It is hoped that our knowledge of the corresponding breakdown and withstand stresses for switching surges in cables and accessories will keep pace.

Section III. Standard Impulse Breakdown Tests on Cables

There has been a marked lack of uniformity in methods of making impulse-breakdown tests on cables. The need of more consistent results in the future calls for standardization of test methods and procedure.

Proposed Standard Test

1. Exclusive of terminal portions, the active length of cable test sample should be not less than three feet and preferably longer. The length and total capacitance of the sample, however, should not be great enough to affect materially the shape of the impulse wave.

2. The test should be made at room temperature with a standard negative 1.5 x 40-microsecond wave. If, due to relatively high capacitance of the test sample, a 1.5 x 40-microsecond wave cannot be exactly adhered to, reasonable variations will be permissible, provided the exact shape and magnitude of the impulse wave is recorded in the test report.

3. The test terminals should be sufficiently stronger than the cable to eliminate all extraneous discharges and, where necessary, should be immersed in oil for this purpose.

4. The impulse test circuit, method of measuring voltage and all other details in AIEE Standard 4 of March 1943, "Measurement of Test Voltage in Dielectric Tests," should be followed.

5. The test should be made by the voltage-step method and each step should consist of five separately applied impulses. The voltage should be increased in equal steps, each ten per cent of the starting voltage, and the starting voltage for the first step should be in accordance with the recommended withstand voltage stresses in Table IV. This should result in final failure in not less than two or more than eight steps. If the first trial departs from this, the starting-step voltage should be readjusted to come within these desired limits.

6. After breakdown the test sample should be dissected and the nature of the failure path noted. Partial failures elsewhere should also be recorded.

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Transmission-Line Electric Loadings

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THE loading of transmission lines depends upon many factors worthy of review at this time. With the removal of restrictions on critical materials, system engineers will give renewed consideration to their fundamental system design, and their effort may be expected to result in careful study to lower costs. This endeavor ultimately will result in standardization of apparatus and of system arrangements of proved designs. It is desirable to take a look into the future at this time, therefore, to determine what the transmission-line design loadings should be in order to make best use of the developments and experience of the past decade. This paper is devoted to an analysis and discussion of some of the important factors determining transmission-line electric loadings.

Transmission systems, by the use of quick switching, relaying, and modern design practices, have overcome their stability limitations to a very considerable extent. Transmission lines, excepting possibly those approaching 300 miles, can now be said to be operated with adequate stability margins. Lines can now be designed which minimize the probability of having faults due to lightning. Furthermore, the use of high-speed reclosing has increased the reliability and performance of transmission lines following short circuits caused by lightning. This progress indicates the possibility of further increasing transmission-line loadings.

The stability limitation for existing transmission lines ordinarily becomes less important as the length of the transmission link decreases, since the loading is not usually correspondingly increased. With the development of interconnected systems in the United States, the tendency is to reduce the length of the straightaway transmission links, that is, length of transmission line without intermediate generation or load. Systems which start with long transmission lines of the order of 300 miles in length change in time to become several shorter links interconnecting local load and generation areas. Accordingly, only a few lines in the country are the pure long-distance transmission lines which have been given so much attention technically. A more usual length of transmission distance is 50 to 100 miles. Few lines of this shorter length are designed for loadings which would be determined by their inherent stability capabilities. If a 220-kv line, 300 miles in length, is capable of transmitting 120,000 kw per circuit, and requiring negligible kilovars for transmission, then a 100-mile line, from a stability standpoint, should be able to transmit with proportional terminal capacities approximately three times as much or 360,000 kw per circuit. Such an increased loading, however, would require synchronous-condenser kilovars (overexcited) or its equivalent of about 210,000 at the receiving end, and an overexcited kilovars at the sending end of about 70,000. This example is for a line which has a line resistance and reactance of 0.16 and 0.80 ohm per mile, respectively. The possibility of increasing the loading of lines of this length depends primarily on being able to use more kilovars for transmission.

The reactive kilovar-hour is a relatively inexpensive quantity compared with the real kilowatt-hour.[1] The cost of kilovars is in the range of about ten dollars per kilovar, whereas the cost of real kilowatt capacity is from $200 to $300 per kilowatt when the operating and fuel costs are evaluated as equivalent investment. With a better understanding of kilovar control it is expected that the trend to increased use of capacitors and synchronous condensers will continue. Heavy transmission-line loadings will require increased amounts of controlled kilovar capacity such as that provided by synchronous condensers. Their use will provide additional lagging capacity which will allow for further use of unswitched shunt capacitors in the load areas.

In addition to the problem of stability and kilovar capacity there is the problem of conductor losses and heating. If the loadings of transmission lines are increased inversely as the line length, with constant stability loading for terminal capacities proportional to the load, the resistance loss per mile varies approximately inversely as the square of the line length or about the same percentage total loss in terms of the transmitted power. That is, for the same conductor and voltage, a 100-mile line would have approximately nine times the loss per mile of conductor as a 300-mile line having one-third the kilowatt loading, while the percentage loss for the whole line is approximately the same. More accurately, the kilowatt loss in per cent of delivered power of the heavily loaded 100-mile line would be somewhat greater, 15 to 20 per cent more, than that of the 300-mile line because of a change in power factor. If resistance is reduced by 15 to 20 per cent, the per cent losses will be equal to those of the 300-mile line. A larger conductor may be necessary or highly desirable, however, for the more heavily loaded shorter lines in order to remain within the thermal limits or to keep the total system transmission losses to reasonable values, since the power in an interconnected system may be in effect transmitted great distances over several transmission-line links.

These relations suggest for consideration the design of transmission links in the future more nearly approaching their stability limits under either normal or emergency conditions. A 100-mile 220-kv line would then have a maximum loading approaching 300,000 to 400,000 kw per circuit and a transmission-line conductor of copper (or equivalent) of about 700,000 circular mils. For a 100-mile 132-kv line this would mean a loading approaching 100,000 to 150,000 kw per circuit and a conductor size of copper (or equivalent) of at least 300,000 to 400,000 circular mils. Fortunately, transmission-line costs do not increase directly with increased conductor size. For example, it is estimated that to increase the conductor size of copper (or equivalent) from 400,000 to 800,000 circular mils for a 220-kv circuit would increase the cost per mile of the transmission line only 25 per cent, not including the cost of the right of way. At the same time, the losses are reduced to one half. An alternative is to increase the line voltage. For moderate additional system line capacity, however, it may not be economical to depart from the present system transmission-line voltage level. In any case, even at the higher voltage it may be advisable to plan for future heavy line loadings.

Although these trends to heavier line loadings and larger line conductors are

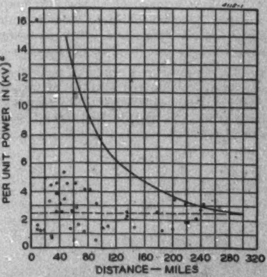

Figure 1. Major transmission-line loadings in United States before World War II

Solid line—Loading based on 25 per cent stability margin with representative terminal capacities—750 kilowatt-miles per $(\text{kv})^2$
Dashed line—Surge-impedance or unity-power-factor loading

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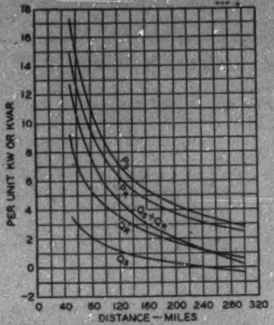

Figure 2. Representative transmission-line loadings and required kilovars for transmission of power when limited by steady-state stability

Unit kw = unit kvar = $(kv)^2$
$x=0.8$ ohm per mile
$r=0.16$ ohm per mile
$y=5.2$ micromhos per mile
P_S = sending-end kilowatts
P_R = receiving-end kilowatts
Q_S = sending-end kilovars
Q_R = receiving-end kilovars
Q_S+Q_R = total kilovars for transmission
Equal sending- and receiving-end voltages
$E_S=E_R$

already evident in practice, the economics will determine how fully they can be realized for a given case. The economic factors are, of course, many and varied, but a few general remarks may be made. The cost of kilovars required to control the voltages under these heavier loadings will limit and modify the loadings which here appear technically possible. Controlled kilovars properly located, however, may make use of the diversity of the kilovar requirements of several transmission links to different areas of the interconnected system. Transmission-line loadings can be dispatched dependent upon kilovar as well as kilowatt capacity. Emergency load requirements may justifiably allow a temporary sagging of voltage at points of limited kilovar capacity. Part of the kilovars required for the transmission may be supplied from generators at a relatively low cost per kilovar. Many tie lines and interconnected systems during the present war emergency have been restricted in the amount of power which they are capable of transferring because of lack of controlled kilovar capacity and because of high incremental I^2R losses. Recognition of the necessity of using increased amounts of controlled kilovar capacity properly located is essential for the realization of heavier line loadings. A-c network analyzers may be used to study present and future system conditions.

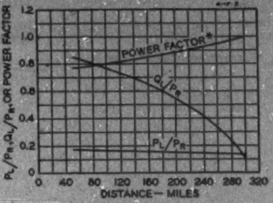

Figure 3. Transmission-line equivalent total power factor* and ratio of transmission-line real and reactive losses to delivered real power for representative transmission-line loadings when limited by steady-state stability (750 kilowatt-miles per $(kv)^2$). See Figure 2

*Equivalent total transmission-line power

$$\text{factor}=\frac{P_R}{\sqrt{Q_L^2+P_R^2}}$$

$$Q_L=Q_R+Q_S \qquad P_L=P_S-P_R$$

If found economically advisable, there are two steps required for increased line loadings:

1. Designing for the use of larger conductors.
2. Providing for controlled kilovars for the transmission of power as the system loads increase.

Fortunately, a large part of the investment required for the full completion of the second step can be deferred until required. *It is of prime importance, however, that the first step be recognized in the design stage so that the line will be built with the larger conductor.*

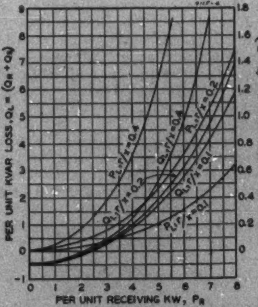

Figure 4. Real and reactive power loss with change in conductor resistance and loading 100-mile line, equal sending- and receiving-end voltages

$x=0.8$ ohm per mile
$y=5.2$ mhos per mile
No-load real losses neglected

Existing Transmission-Line Loadings

The rated or design loadings of most of the existing important transmission lines in the United States prior to World War II are given in reference 2. These line loadings were referred to a common base by dividing the rated received kilowatts per circuit by the square of the receiver voltage in kilovolts. These per unit loadings plotted against line distance in miles are shown in Figure 1. As is shown in reference 3 the surge impedance or unity-power-factor loading on this common base is approximately 2.5 $(kv)^2$ for practically all transmission lines. This loading has been also termed the "natural loading" and corresponds closely to the condition of unity-power-factor transfer of power along the line. Thus at this loading, practically no kilovars are required for the transmission of power. The I^2X loss in the line equals the line-charging volt-amperes at this loading. As shown for the lines above 100 miles, the kilowatt loadings have not exceeded greatly the surge-impedance loading. Even for lines of below 100 miles in length, with the exception of one ten-mile line, the loadings do not exceed about twice the surge-impedance loading.

Since the stability limit of a transmission line with terminal system capacities proportional to the transmitted load varies approximately inversely with the line length, it is evident that the shorter lines are operating with larger stability margins than the longer line. To show this, a curve indicating the loadings at which stability would become limiting for representative terminal capacities is shown in Figure 1, based on the indicated experience that a loading of 2.5 $(kv)^2$ for 300 miles is practical. This curve was drawn from the relation

$$P=2.5(kv)^2\left(\frac{300}{l}\right)=750\frac{(kv)^2}{l} \text{ in kw} \qquad (1)$$

wherein l = length in miles. A higher or lower loading than 750 kilowatt-miles per

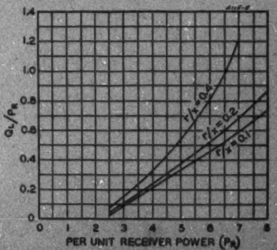

Figure 5. Variation of ratio of kilovar loss to receiver power for conventional 100-mile line with change in loading and conductor resistance

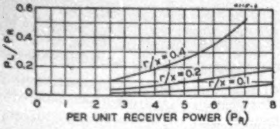

Figure 6. Variation of ratio kilowatt loss to receiver power for conventional 100-mile line with change in loading and conductor resistance

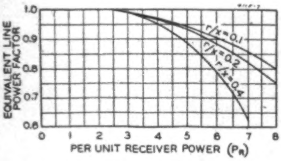

Figure 7. Equivalent total line power factor for conventional 100-mile line versus line loading with change in conductor resistance

(kv)^2 may be used as the design loading depending on the terminal capacities as well as the margin required to ride through transient disturbances. Since this value has been obtained in practice and is considered to be a relatively high loading, it is used here for purposes of comparison. It is of interest to note that it is possible to carry a loading of 2.5 (kv)^2 775 miles (one-quarter wave length) with infinite terminal capacities at either end[3] or a loading of about 1,900 kilowatt-miles per (kv).^{2} The question naturally arises as to why the shorter transmission lines are not worked harder. The answer to this question is given partly by Figure 2. This shows the total kilovars of the sending and receiving end (Q_S+Q_R)* which are required for transmitting the power for the loading shown by the curve in Figure 1 with equal terminal voltages. As the kilowatt loading increases, the kilovars required for the transmission of power increases. At some loading this increase in kilovars becomes a limiting factor, and it would, therefore, be more economic to build a second line or use a higher voltage than to increase further the loading per (kv).^{2} The most economical loading depends upon the proper consideration of all the designs and economic factors and can be expected to differ between systems.

Stability

The factors which determine stability have been given a great deal of attention. The use of modern apparatus and a design which considers the stability characteristics have resulted in greatly improved reliability. The amount of power which can be transferred per circuit is becoming more and more limited by the steady-state stability limit of the line and system. As has been shown,[3] this is the primary limitation for 60-cycle conventional lines approaching 300 miles or more in length. At this length the design loading usually will correspond to about the surge-impedance or unity-power-factor loading of 2.5 (kv).^{2} The approximate surge-impedance loadings are given in Table I for different line voltages. These can be considered to be loadings which have been obtained with reliability with conventional transmission lines of about 300 miles in length.

Column 2, Table I, shows loads which may be transferred 300 miles with a 25 per cent stability margin for a system with reasonable terminal capacities.[3] Column 2, Table II, shows the loadings possible for a 100-mile line with the same stability margin and comparable terminal capacities, proportional to the delivered power, as for the 300-mile lines. Column 3, Table II, shows the total or *sum* of sending and receiving kilovars required for the transmission of the loads given in column 2, Table II. It will be noted that three times as much power may be transferred per circuit for the 100-mile transmission line as was transferred with the 300-mile line if for each eight kilowatts transferred there is provided six kilovars for transmission. For a line having an r/x ratio equal to 0.2 and equal sending- and receiving-end voltages, the power factor at the sending end would be 0.98 and the power factor at the receiving end 0.86. If the kilowatt loading is reduced to 5.0 (kv),^{2} the kilowatts transmitted for various circuit voltages for 100 miles is given in column 4 of Table II. Column 5 gives the corresponding total kilovars for transmission. In this case for each 5.0 kw transmitted, 2.0 kilovars is required. The loading of column 4 does not appear unreasonable for a 100-mile line since the kilovars can be supplied from both ends at high power factors. For purposes of analysis and discussion, it is well to consider the two quantities kilovars and kilowatts, rather than kilowatts and power factor.

For purposes of comparing stability loadings of transmission lines, it is convenient to rate lines in terms of kilowatt-miles per (kv).^{2} Accordingly, to this measuring stick the representative loading given by equation 1 is 750 kilowatt-miles per (kv).^{2} Columns 2 and 4 of Table II have stability ratings of 750 and 500 kilowatt-miles per (kv),^{2} respectively.

Losses

Since stability is not necessarily a limiting factor for shorter lines, consideration must be given to losses. Figure 2 shows the kilovars at the sending and receiving ends of the line for equal terminal voltages when the loading is 750 kilowatt-miles per (kv).^{2} Characteristics shown on Figure 2 are for a line having a reactance of 0.8 ohm per mile, a capacitive admittance of 5.2 micromhos per mile and an r/x ratio of 0.2. Although such a loading may be uneconomical at 100 miles because of the kilovars required for transmission, it is of interest to consider this loading further as it is a representative upper stability limit.

The ratio of line power loss to received or delivered power, as well as the ratio of total sending- and receiving-end reactive power to the receiver power is plotted in Figure 3 versus distance in miles for the line of Figure 2. The real losses as shown vary from 13.5 per cent of the delivered power for a 300-mile line to 17 per cent for a 50-mile line, when the line is loaded at a constant value of kilowatt-miles per (kv).^{2} The kilovar requirements, however, increase rather rapidly with decrease in distance, if a constant stability loading of 750 kilowatt-miles per (kv)^2 is maintained. Also on Figure 3 is plotted what is termed here as the equivalent transmission-line power factor defined by the equation

Equivalent transmission-line power factor

$$=\frac{P_R}{\sqrt{Q_L{}^2+P_R{}^2}}$$

where

P_R = receiving power

$Q_L=Q_R+Q_S$ = the sum of the sending and receiving kilovars

This equivalent power factor may be used as a measure for comparing the reactive loading of a transmission line. The sum of the sending and receiving kilovars when expressed in terms of a percentage of the receiver power (Q_L/P_R) can be used instead of the total line power

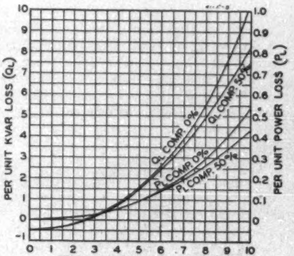

Figure 8. Total real and reactive power losses for 100-mile line with and without series-capacitor compensation

Y = 5.2 micromhos per mile
x = 0.8 ohm per mile
r = 0.04 ohm per mile
No-load real losses neglected

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* The sign convention of both the sending-end and receiving-end reactive power Q_S and Q_R, respectively, are taken as positive, corresponding to overexcited kilovars into the transmission line.

factor as a measure of the reactive loading. This is a convenient method, since the total kilovars remains essentially constant while the ratio between the sending and receiving kilovars depends upon the ratio of the magnitude of voltages held at either end. For the line whose characteristics are given in Figure 2, it was assumed that the sending and receiving voltages were equal, and it will be noted that in this case the required receiving-end kilovars is approximately twice the sending kilovars. Since a heavily loaded transmission line may have overexcited kilovars supplied at both ends for transmission of the power, it is evident that the terminal power factors are far from constant along the line and that changes in terminal power factors do not indicate directly the increase in losses. As systems become more interconnected, it becomes more desirable to hold the transmission-line voltages at constant values independent of the direction or amount of power flow. The condition of equal sending- and receiving-end voltages, therefore, represents a typical condition for a study of the transmission links of an interconnected system. If the sending-end voltage is increased relative to the receiving end, the sending-end kilovars is increased and the receiving-end kilovars reduced although the total kilovars is not changed appreciably.

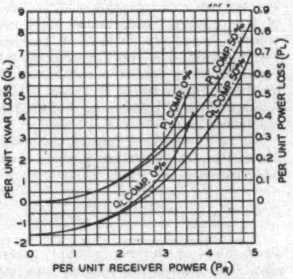

Figure 9. Total real and reactive power losses for 300-mile line with and without series capacitor compensation

$Y = 5.2$ micromhos per mile
$x = 0.8$ ohm per mile
$r = 0.08$ ohm per mile
No-load real losses neglected

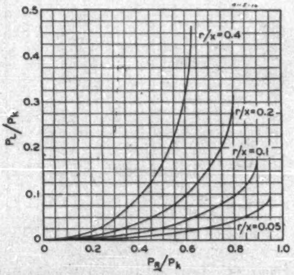

Figure 10. Power-loss characteristics

No-load losses neglected

The effect of change in conductor resistance of a 100-mile line is given by Figure 4. In this and the subsequent figures the component of losses which are independent of loading with fixed terminal voltages have been neglected, that is, results are based on a π representation without losses in the shunt branches. Both the kilowatt and kilovar losses increase with increase in line resistance for the same line reactance. As will be noted, the real power losses increase directly with increase in the resistance for moderate loadings. As the loadings are increased, however, the real losses increase disproportionately for the higher resistance lines because of the change in power factor. This indicates the desirability of using low-resistance conductors on heavily loaded lines even more than would be indicated by assuming the losses to increase directly with the line resistance.

The kilovar requirements are not greatly different with change in conductor resistance for the same line reactance except for the higher loadings. At the higher loadings there is an appreciable increase in the kilovar requirements, particularly for a line having an r/x ratio equal to 0.4. An additional advantage with the use of a low-resistance conductor is a more equal distribution of kilovars between the sending and receiving ends. For example, with equal sending- and receiving-end voltages the values of sending and receiving kilovars are obtained for a 100-mile line with different r/x ratios when loaded at 500 kilowatt-miles per (kv),[2] as shown in Table III. This may represent an important advantage since the receiving system is usually required to provide kilovars for the load and is not as able as the sending end to provide kilovars for transmission. Recognition of the kilovar requirement for both ends of an interconnection has not always been fully appreciated by interconnected operating companies. The use of low-resistance conductors is in the direction to distribute better the kilovar requirements between the sender and receiver and still maintain the voltage levels.

Figures 5 and 6 show the ratio of the reactive losses to the receiver power and the ratio of real losses to the receiver power, respectively. These curves show an increasing percentage of real and reactive losses as the loading increases, with a greater percentage increase for the high-resistance lines. Figure 7 shows the equivalent line power factor plotted against receiver power for different ratios of r/x. All of these characteristics indicate the desirability of using low-resistance conductors in order to reduce the real and reactive losses, particularly at the higher line loadings. The low-resistance lines also have a higher stability limit in terms of receiver power.

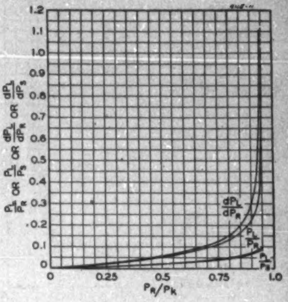

Figure 11. Total and incremental losses for $r/x = 0.05$

No-load losses neglected

As the circuit voltage increases, the r/x ratio generally decreases since the reactance per unit length remains essentially constant. The unity-power-factor current loading of a 220-kv line is 315 amperes, whereas that of 132-kv line is 202 amperes. The line current increases directly with the circuit voltage for the same line loading in kilowatt-miles per (kv).[2] Accordingly, high-voltage lines ordinarily will have low r/x ratios in order to keep the current density down as well as from a corona and mechanical strength standpoint. The lower-voltage lines, below 220 kv, of an inter-connected system may on the other hand have fairly high r/x ratios.

Series capacitors have been proposed as an effective method of overcoming the stability limitations of long lines;[3] studies also have been made as to their practicability for shorter lines. Figures 8 and 9 show the total real and reactive power losses for a 100- and a 300-mile line, re-

Table I. Approximate Transmission-Line Surge-Impedance Loading Per Circuit for Conventional* Overhead A-C Lines

Voltage—Kv	Surge-Impedance Loading—$Q_L/P_R = 0$ Delivered Kw = 2.5 Kv[2]
330	270,000
275	190,000
220	120,000
154	60,000
132	44,000
110	30,000
66	11,000

* Values in table are for line having a reactance, resistance, and capacity susceptance of 0.8 ohm per mile, 0.16 ohm per mile, and 5.2 micromhos per mile, respectively.

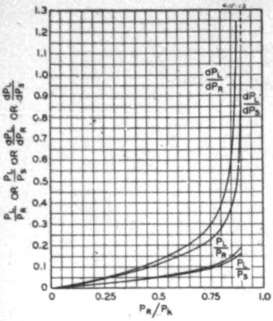

Figure 12. Total and incremental losses for $r/x=0.10$

No-load losses neglected

spectively, without series-capacitor compensation and with 50 per cent series-capacitor compensation infinitely distriuted. Figure 8 for the 100-mile line shows that up to and exceeding a loading 7.5 $(kv)^2$ the total reactive losses do not differ greatly with or without series compensation. Accordingly, for loadings up to this value there is no appreciable difference in the losses or in the total corrective capacity required in series compensation plus controlled shunt compensation. At a loading of 7.5 $(kv)^2$ the series-capacitor kilovars for the 50 per cent compensated line is 57 per cent of the total or 0.57 × 3.70 $(kv)^2$ = 2.10 $(kv)^2$ or 38,500 kilovars for a 132-kv line requiring a total of 65,000 kilovars. For loadings above 7.5 (kv),[2] however, series compensation probably will be required for stability purposes in order to allow for a line loading exceeding 750 kilowatt-miles per (kv^2) and as shown by Figure 8 to reduce the real and reactive line losses. Small stability margins and high line losses are usually synonymous.

Table II. Approximate Real and Reactive Transmission-Line Loadings Per Circuit of Conventional* 100-Mile Overhead A-C Lines

Two and Three Times Surge-Impedance Loading or 750 Kilowatt-Miles Per $(Kv)^2$ and 500 Kilowatt-Miles Per (Kv),[2] Respectively

	750 Kw-Miles/$(Kv)^2$ $Q_L/P_R=0.75$		500 Kw-Miles/$(Kv)^2$ $Q_L/P_R=0.40$	
Voltage Kv	Delivered Kw = 7.5 $(Kv)^2$	Total Kvar = 5.62 $(Kv)^2$	Delivered Kw = 5.0 $(Kv)^2$	Total Kvar = 2.0 $(Kv)^2$
330	810,000	610,000	540,000	216,000
275	570,000	430,000	380,000	152,000
220	360,000	270,000	240,000	96,000
154	180,000	135,000	120,000	48,000
132	132,000	99,000	88,000	35,000
110	90,000	67,500	60,000	24,000
66	33,000	25,000	22,000	8,800

* Values in table are for line having a reactance, resistance, and capacity susceptance of 0.8 ohm per mile, 0.16 ohm per mile, and 5.2 micromhos per mile, respectively.

Also as shown by Figure 9 for the 300-mile line, series compensation is advantageous only for loads above about 2.5 $(kv)^2$ or for a stability loading exceeding 750 kilowatt-miles per (kv).[2] As in the case of the 100-mile line above this stability loading (750 kilowatt-miles per $(kv)^2$) the series-capacitor compensation begins to show advantages. In general, it can be concluded that unless the circuit loading required is high or stability is a limitation, series capacitors have little advantage over controlled shunt compensation. A series-compensated line also requires controlled shunt compensation for control of line voltage to correct for the variable I^2X of the line. These results indicate that, as the distance decreases and the stability limitations become less severe, it is more economical to use controlled kilovar capacity in shunt such as is provided by a synchronous condenser. In this way the corrective capacity is better used in that it makes use of the diversity of the kilovar requirementat a given bus or circuit connection point rather than being allotted to a given circuit or pair of circuits. Also for short lines the series capacitor may act as a limit to the amount of power which may be transferred over a given circuit during an emergency. Accordingly, the use of series capacitors for major transmission lines appears to be more applicable to the longer straightaway transmission lines where stability is the major limitation, whereas for shorter lines with lower stability loadings controlled kilovars used in shunt relation is more economical of corrective capacity. Controlled shunt compensation allows for greater loadings than are now generally used, whereas series capacitors with controlled shunt compensation allow for even greater line loadings.

Incremental Losses

In the operation of an interconnected system, loads to be delivered at some point, in effect, may require generation from a remote point. The transmission of this load is then superimposed upon the existing loads being transmitted by the various links of the power system. The cost of transmitting this power, therefore, properly involves a consideration of the incremental losses of the various transmission-line links. A load of this nature may unload some lines, since it will tend to reverse the direction of initial power flow and will increase the loading on other links. The appendix gives the derivation of equations for determining the incremental power losses when the transmission line has equal sending- and receiving-end voltage. If the voltages are different, good approximate results still can be obtained for total real and reactive power losses. It is shown that the total power loss and incremental power losses are a function only of the ratio of line resistance to reactance and the ratio of the receiver power to the total line transfer impedance divided by the line voltage squared. The line transfer impedance can be approximated accurately as the square root of the sum of the squares of the line resistance and the line reactance.

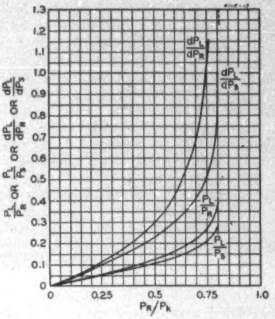

Figure 13. Total and incremental losses for $r/x=0.20$

No-load losses neglected

Figure 10 shows the ratio of the power loss to P_k, plotted against the ratio of receiver power to P_k, where

$$P_k=\frac{E^2}{\sqrt{r^2+x^2}}$$

These ratios are numeric and apply generally. The ratio of line resistance to line reactance is used as a parameter for the various curves plotted. As shown by Figure 10, the real power loss increases rapidly with increased line loading, par-

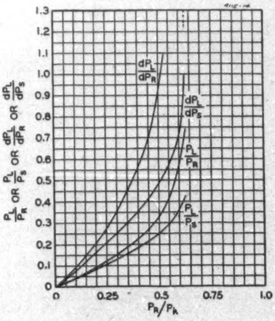

Figure 14. Total and incremental losses for $r/x=0.40$

No-load losses neglected

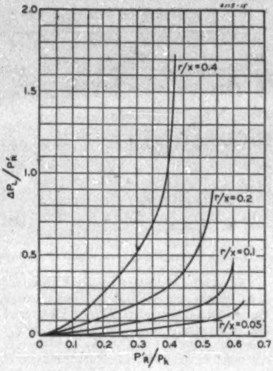

Figure 15. Block incremental power loss

$$\frac{\Delta P_L}{P_R'}=\frac{P_L''-P_L'}{P_R'} \text{ where } P_R''=1.5P_R'$$

No-load losses neglected

ticularly near the receiver pull-out point with infinite terminal capacities, that is, infinite busses at either end of the line. Figures 11, 12, 13, and 14 show the total and incremental losses for lines having r/x ratios of 0.05, 0.10, 0.20, and 0.40, respectively. On these curves the incremental losses defined both as a ratio of the change in power losses to the change in sending- or receiving-end powers are plotted. Also plotted is the ratio between the total power losses and the sending- and receiving-end power. The lower-resistance lines are characterized by their ability to carry considerably greater increases in load before a disproportionate increase in the losses occur. Since the curves are for constant r/x ratio, the incremental-power-loss characteristics need only be multiplied by the ratio x/r to determine the kilovar incremental losses.

It will be noted that the ratio of the losses referred to the receiver power is naturally higher than when the ratio is referred to the sending-end power. In evaluating incremental losses and power losses it generally will be advisable to refer the losses to the receiving-end power. The incremental losses approach infinity at the maximum receiver power when the maximum receiver power is determined with infinite terminal capacities at either end of the line section under consideration.

Figures 15 and 16 express the incremental power in terms of "block" incremental power loss. The previous curves gave the incremental power in terms of a small or additional increase in loading, whereas Figures 15 and 16 are for increases in loading for a block of power equal to 50 per cent and 100 per cent above the original power being transferred, respectively. As would be expected, these have the same characteristic shape as the other incremental-power-loss curves, except that they result in an incremental-loss coefficient which is between that determined on the basis of the incremental-loss coefficient for the first additional kilowatt above the initial load and the last additional kilowatt above the total load. These results show that both the real and reactive power losses of a line for a given amount of delivered power are appreciably decreased by the use of low-resistance conductors.

Summary and Conclusions

1. The following guides may be used in estimating possible electric loadings for 60-cycle lines, with the understanding that any particular case should be investigated thoroughly:

(a). Surge impedance loading............2.5 (kv)^2

(b). Reasonably heavy stability line loading...........750 kilowatt-miles/(kv)^2

(c). Series-capacitor compensation becomes particularly effective for line loadings above about.......750 kilowatt-miles/(kv)^2

2. Transmission-line real losses at the heavier line loadings are reduced more than directly with the conductivity of the line conductors.

3. Transmission-line kilovar losses are reduced by the use of lower-resistance conductors for a given receiver power, particularly for the heavier line loadings.

4. Lower-resistance conductors

(a). Increase the stability limit in terms of delivered power.

(b). More equally divide the kilovar requirement for transmission between sending and receiving ends.

5. Developments in the past decade which have improved the transmission-line reliability allow for the use of heavier line loadings.

6. Increased transmission-line loadings are possible through the use of increased controlled kilovar capacity such as synchronous condensers, particularly for transmission lines less than 300 miles in length.

7. Series-capacitor compensation of line reactance provides a method by which line loadings may be increased above that possible by controlled shunt kilovars only. Line-reactance compensation is most effective when stability is the chief limitation for higher loadings, that is, loadings above about 750 kilowatt-miles per (kv)^2. Therefore this method is more attractive for longer lines than for shorter lines.

Table III

r/x	0.1	0.2	0.4
Q_R	+1.3	+ 1.9	+3.4
Q_S	+0.5	+ 0.1	−0.8
$Q_L=Q_R+Q_S$	+1.8	+ 2.0	+2.6
Q_R/Q_S	+2.6	+ 19.0	−4.2

8. With the development and continued interconnection of power systems in the United States, and other countries, it is expected that greater use will be made of controlled kilovar capacity and of conductors of lower resistance than heretofore has been the general practice.

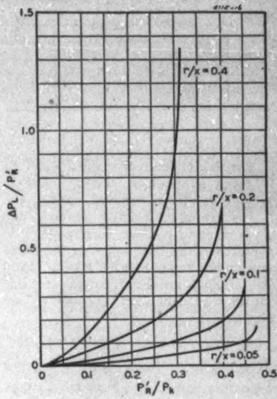

Figure 16. Block incremental power loss

$$\frac{\Delta P_L}{P_R'}=\frac{P_L''-P_L'}{P_R'} \text{ where } P_R''=2.0P_R'$$

No-load losses neglected

Nomenclature

All quantities are in per unit.

E = line voltage
P = real power
Q = reactive power
δ_{12} = electrical angular displacement between sending and receiving ends
Z = impedance
Z_{11} = driving point impedance from sending end
Z_{22} = driving point impedance from receiving end
Z_{12} = transfer impedance
$\alpha = 90 - \theta$
θ = impedance angle corresponding to subscripts
$P_k = \dfrac{E^2}{\sqrt{r^2+x^2}}$
r = resistance of line corresponding to line equivalent circuit
x = reactance of line corresponding to line equivalent circuit

Subscript S refers to the sending end, point 1, of the network equivalent of the transmission line (Figure 17). Subscript R refers to the receiving end, point 2, of the network equivalent.

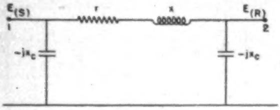

Figure 17

The sign of the real power P_S and P_R is taken as positive, corresponding to power sent out and received, respectively.

The sign of the reactive power Q_S and Q_R is taken as positive, corresponding to overexcited reactive power to the transmission line to provide I^2X losses. When the terminals are supplying line-charging capacity at light load, Q_R and Q_S are negative.

Appendix. Real and Reactive Transmission-Line Power Losses

A line segment may be represented by a π line (either nominal or equivalent). See Figure 17. The following equations may be written for the sending- and receiving-end real and reactive power in per unit:[4]

$$\left.\begin{aligned} P_S&=\frac{E_S^2}{Z_{11}}\sin\alpha_{11}+\frac{E_SE_R}{Z_{12}}\sin(\delta_{12}-\alpha_{12})\\ P_R&=-\frac{E_R^2}{Z_{22}}\sin\alpha_{22}+\frac{E_SE_R}{Z_{12}}\sin(\delta_{12}+\alpha_{12})\end{aligned}\right\}\quad(2)$$

$$\left.\begin{aligned} Q_S&=\frac{E_S^2}{Z_{11}}\cos\alpha_{11}-\frac{E_SE_R}{Z_{12}}\cos(\delta_{12}-\alpha_{12})\\ Q_R&=\frac{E_R^2}{Z_{22}}\cos\alpha_{22}-\frac{E_SE_R}{Z_{12}}\cos(\delta_{12}+\alpha_{12})\end{aligned}\right\}\quad(3)$$

The real and reactive line losses are $P_L=P_S-P_R$ and $Q_L=Q_S+Q_R$, respectively.

From equations 2 and 3 with $E_S=E_R=E$ and $Z_{11}=Z_{22}$,

$$P_L=\frac{2E^2}{Z_{11}}\sin\alpha_{11}+\frac{E^2}{Z_{12}}\times\left[\sin(\delta_{12}-\alpha_{12})-\sin(\delta_{12}+\alpha_{12})\right]\quad(4)$$

or

$$P_L=\frac{2E^2}{Z_{11}}\sin\alpha_{11}-\frac{2E^2}{Z_{12}}\sin\alpha_{12}\cos\delta_{12}\quad(5)$$

Also,

$$Q_L=\frac{2E^2}{Z_{11}}\cos\alpha_{11}-\frac{E^2}{Z_{12}}\times\left[\cos(\delta_{12}-\alpha_{12})+\cos(\delta_{12}+\alpha_{12})\right]\quad(6)$$

or

$$Q_L=\frac{2E^2}{Z_{11}}\cos\alpha_{11}-\frac{2E^2}{Z_{12}}\cos\alpha_{12}\cos\delta_{12}\quad(7)$$

Since $E_1=E_2=E$, and if the losses in the admittance branches are negligible, that is, if

$$\frac{\sin\alpha_{11}}{Z_{11}}=\frac{\sin\alpha_{22}}{Z_{22}}=\frac{\sin\alpha_{12}}{Z_{12}}$$

equations 2 and 5 yield

$$P_R=-\frac{E^2}{Z_{12}}\sin\alpha_{12}+\frac{E^2}{Z_{12}}\sin(\delta_{12}+\alpha_{12})\quad(8)$$

$$P_L=\frac{2E^2}{Z_{12}}\sin\alpha_{12}-\frac{2E^2}{Z_{12}}\sin\alpha_{12}\cos\delta_{12}\quad(9)$$

If equation 8 is solved for $\cos\delta_{12}$ in terms of P_R

$$\cos\delta_{12}=-\sin\alpha_{12}A\pm\cos\alpha_{12}\sqrt{1-A^2}\quad(10)$$

where

$$A=-\frac{P_RZ_{12}}{E^2}-\sin\alpha_{12}=-\frac{P_R}{P_k}-\sin\alpha_{12}$$

If equation 10 is substituted in equation 9, using the positive sign for the second term, corresponding to values of δ_{12} in the stable operating range,

$$P_L=\frac{2E^2}{Z_{12}}\sin\alpha_{12}-\frac{2E^2}{Z_{12}}\times\sin\alpha_{12}(\cos\alpha_{12}\sqrt{1-A^2}-\sin\alpha_{12}A)\quad(11)$$

or

$$\frac{P_LZ_{12}}{E^2}=2\sin\alpha_{12}-2\sin\alpha_{12}\times(\cos\alpha_{12}\sqrt{1-A^2}-\sin\alpha_{12}A)\quad(12)$$

If P_L is differentiated with respect to P_R to determine the incremental power losses,

$$\frac{dP_L}{dP_R}=-2\sin^2\alpha_{12}-\frac{2\sin\alpha_{12}\cos\alpha_{12}A}{\sqrt{1-A^2}}\quad(13)$$

or

$$\frac{dP_L}{dP_R}=-2\sin^2\alpha_{12}+\frac{2\sin\alpha_{12}\cos\alpha_{12}\left(\frac{P_R}{P_k}+\sin\alpha_{12}\right)}{\sqrt{1-\left(\frac{P_R}{P_k}+\sin\alpha_{12}\right)^2}}\quad(14)$$

The incremental reactive power losses are equal to the incremental real power losses multiplied by the ratio of x/r. Therefore, the incremental real and reactive power losses for a given ratio of line resistance to reactance is only dependent on the ratio of the receiver power times the total line impedance divided by the line voltage squared.

The incremental losses at the sending end can be determined from equation 14 by use of the relation,

$$\frac{dP_L}{dP_S}=\frac{dP_L}{dP_L+dP_R}=\frac{\frac{dP_L}{dP_R}}{1+\frac{dP_L}{dP_R}}\quad(15)$$

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New Test Chambers for Aircraft Electric Apparatus With Particular Reference to Carbon Brushes

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Synopsis: When electric machines were taken to high altitudes of 25,000 to 40,000 feet on modern military aircraft, the brushes wore rapidly, insulation sometimes failed, lubrication was uncertain, windings overheated, and other vexing problems appeared. New materials and methods were needed for this new environment.

To accelerate the development of aircraft apparatus, air-conditioned chambers are provided to duplicate the cold, clean, dry, rarefied air of the stratosphere. Facilities being used to test a range of equipment from totally enclosed one-watt computer motors to pressure-ventilated 40-kva alternators[1] are described. Reasons are given for the selection of specific equipment.

From simulated-high-altitude tests under controlled conditions, designers are more quickly completing new apparatus, are better predicting its performance, and are reducing the amount of flight testing required to develop new aircraft.

MODERN AIRCRAFT must fly through all kinds of weather—wet and dry, hot and cold, low and high altitudes. The electric apparatus is further stressed by unusually high speeds, current densities, temperatures, and mechanical forces to meet design limitations on size and weight. Electrical loads change abruptly as a plane enters or leaves combat. To predict over-all performance, tests are required over a wide range of atmospheric and load conditions.

Actual flights are essential for final checks, but quicker methods are needed for the preliminary valuation of new materials at high altitudes. To permit uninterrupted investigations under the direct supervision of apparatus designers, air-conditioned chambers are used to make factory tests at the limiting conditions to which aircraft are exposed. To obtain the maximum benefit from simulated trials, stratosphere conditions should be accurately reproduced.

This paper pertains to the carbon-brush problems on aircraft, the investigations which lead to the construction of special altitude chambers, and a description of how these facilities are being used for general tests on aircraft apparatus.

Nature of Carbon-Brush Problems on Aircraft

Aircraft auxiliaries present formidable problems of commutation, temperature, and brush wear at sea level. At higher altitudes these difficulties are accentuated by atmospheric changes.

More than 11 years ago at the Commonwealth Edison Company[2] in Chicago and the Aluminum Company of Canada, it was found that metal–graphite brushes sometimes wore out in an hour or less on the collector rings of certain synchronous converters during cold weather, when the ventilating air had less than about 1.25 grains of moisture per cubic foot. At that time the trouble was alleviated by humidifying the inlet air whenever it had less than 1.5 grains of water per cubic foot.

Later, in 1939, R. H. Savage[10] further investigated the effects of atmospheric components by admitting individual gases into an evacuated bell jar in which a brush was pressed against a rotating disk. His experiments with carbon on copper (Figure 1) indicated that brush behavior is sharply influenced by water vapor, oxygen, ether, ammonia, organic vapors, and certain other contaminants. Increases in brush friction and wear rate of the order of 5 and 1,000 times, respectively, were typical as gas pressures were reduced. Other gases such as nitrogen and hydrogen are relatively inert in these respects. Conventional brushes were shown to be many times more sensitive to moisture than to oxygen. The worst observed wear occurred with water-vapor pressures of less than one millimeter of mercury, or in the range below one grain per cubic foot.

From a comparison of Savage's test conditions with the atmospheres[3] through which proposed military aircraft were likely to fly (Figure 2), short brush life at high altitudes was predicted. Work was commenced on possible solutions for this problem, and available data were disclosed to other manufacturers of brushes and aircraft apparatus and to the Army Air Corps. Soon thereafter, the above prophesy was fulfilled. Brushes

Figure 1. Influences of water vapor and oxygen on the wear of carbon brushes against a clean, rotating copper disk

Data from R. H. Savage

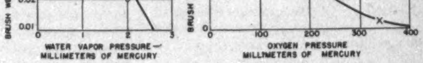
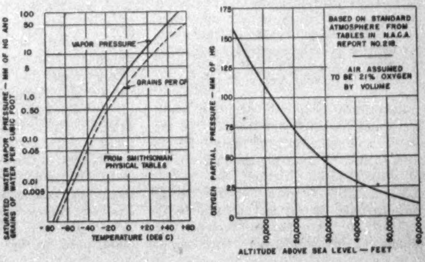


Figure 2. Range of water vapor and oxygen partial pressures in the atmospheres through which aircraft are likely to fly



Paper 44-231, recommended by the AIEE committee on air transportation for presentation at the AIEE Los Angeles technical meeting, Los Angeles, Calif., August 29–September 1, 1944. Manuscript submitted June 6, 1944; made available for printing July 28, 1944.

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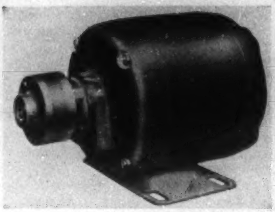
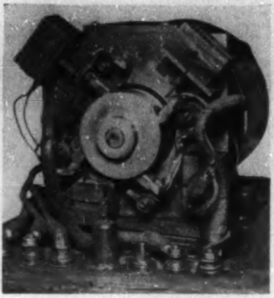
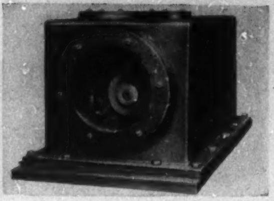


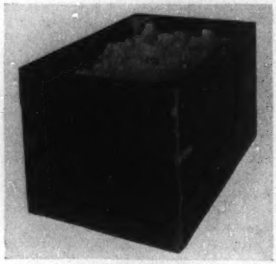
A. A $2^{5}/_{8}$-inch-diameter ring on shaft extension of one-half-horsepower induction motor



B. Assembly of brush rigging and electric gauges G_1 and G_2 to measure brush wear



C. Airtight enclosure with observation window for controlled-atmosphere tests



D. Refrigeration of enclosure C with dry ice (CO_2) which sublimes at −80 degrees centigrade

Figure 3. Preliminary tests of new materials for collector rings, commutators, and brushes

Equipment of L. H. Stauffer

sometimes did not last through one flight at altitudes above 30,000 feet. Knowledge of the causes of these troubles did not suggest obvious remedies. High vapor pressures of water could not be maintained at 50 degrees centigrade below zero. Practical factors prohibited enclosing and supercharging all electric apparatus with compressed air or other vapors.

Conventional brushes when taken to the stratosphere were in a new environment to which they were not adapted. Available brush grades were the result of a natural evolution. Brush manufacturers had tried many materials and continued with only those which performed well in the low-altitude atmosphere where they had been used. New materials were needed for new conditions.

Methods of Attacking Aircraft Brush Problems

The phenomena of sliding contacts are not well understood, but it seemed apparent that the smooth commutator films formed in wet oxygen-rich air must be imitated at high altitudes by one of the following methods:

1. *New Brush Materials.* This was the most attractive approach. New brushes can be put in existing equipment.

2. *New Alloys for Commutators and Collector Rings.* This possibility was less attractive, because new commutators cannot be installed on units in service.

3. *Conditioned Atmosphere.* This was a third choice. It required design changes in planes as well as in electric apparatus. Blast-cooled and self-ventilated units would overheat if enclosed. A suitable atmosphere, if produced, would be difficult to distribute in volume to apparatus throughout a plane.

Laboratory tests indicated that brush life could be improved by each of the above methods, but the initial efforts were mainly on impregnation treatments to reduce brush friction. On March 27, 1942, C. J. Herman and J. F. Settle flew to over 30,000 feet altitude with Colonel H. H. Tellman to correlate laboratory and flight tests. Two amplidyne sets were mounted in the fuselage. "Treated" brushes were put on one set and standard brushes on the other. Rapid wear (only two hours indicated life) of standard brushes began at 25,000 feet on the ascent and ceased at 15,000 feet on the descent. The wear of the "treated" brushes was negligible on this flight, but much more developmental work was needed to obtain satisfactory over-all performance.

Brush tests are influenced by many variables and are inherently erratic. To judge new materials, numerous trials are needed. Flight tests are not always obtainable, and cannot be directly witnessed by apparatus designers. A large plane requires over 1,000 gallons of gasoline to get a three-hour test at 35,000 feet. Pilots and observers cannot remain much longer at such altitudes in unsupercharged cabins even though wearing oxygen masks. Many types of equipment need to be tested concurrently. Flight data on one device are necessarily delayed by adjustments to the others, by airplane maintenance, and local weather conditions.

In spite of difficulties, the Army Air Forces and others obtained helpful flight tests. Comparisons of flight and laboratory data suggested improved factory methods, and further emphasized the sensitivity of carbon brushes to atmospheric changes. It was found that accumulated vapors from lubricants, varnish solvents, fresh paint, or thermal insulation inside a chamber might lubricate brushes and give misleading optimistic life indications by ratios of 100 to 1 or more. Air at high altitudes is free of contaminants, and factory test chambers should be likewise.

Clean, ventilated chambers, in which desired altitude conditions can be duplicated, are now expediting over-all developmental tests on brushes, lubricants, ventilation, bearings, insulation, and other machine parts. All of these things should be studied together, because the performance of each part depends on the condition of the others.

To utilize test facilities effectively, investigations are usually made in the following sequence:

1. Preliminary tests of component parts.

2. Sea-level and altitude-chamber tests of complete machines.

3. Flight tests on things which survive 1 and 2.

Typical facilities being used in this method of attack will now be described.

Preliminary Tests of New Materials for Collector Rings (or Commutators) and Brushes

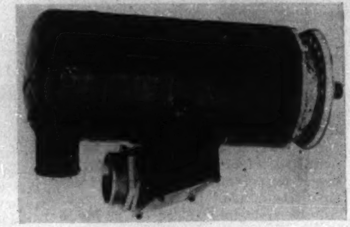
Collector-ring tests are used to avoid building unfavorable materials into complete machines. Special alloys that are difficult to obtain in commutator bar shapes are cast into rings, machined, and mounted on the shafts of small motors as in Figure 3. Dummy commutators are used similarly. Brush holders attached to a mounting plate permit basic data on brush friction, contact drop, temperature, rate of wear, and other characteristics. The speed, commutator diameter, brush holders, spring pressure, current density, and temperature of specific machines are duplicated.

With such equipments L. H. Stauffer has made over 1,000 tests on approximately 300 brush grades and 60 different alloys. By enclosing the test units in a controlled atmosphere, various altitude conditions are simulated. Refrigeration is obtained with dry ice on the outside of the cover. Mixtures of nitrogen and oxygen at the desired partial pressures are pumped through the enclosure. Low humidity is obtained with an activated-alumina dryer similar to that of Figure 7. Electric gauges allow continued readings of brush wear when the sets are enclosed. A variable-frequency power supply for the induction motors permits speeds up to 10,000 rpm.

These preliminary trials are not conclusive, and are not a direct measure of commutation. However they have correlated quite well with altitude chamber and flight data, and permit selection of the more promising materials for exhaustive tests on aircraft apparatus.

A. Totally enclosed d-c gear motor with approximately one-watt output, $1\frac{3}{8}$-inch outside diameter

B. Self-ventilated one-horsepower d-c motor, $4\frac{1}{2}$-inch diameter



C. Pressure-ventilated nine-kilowatt d-c generator, six-inch diameter

D. Pressure-ventilated 40-kva generator with built-in exciter, $9\frac{1}{2}$-inch diameter

Figure 4. Typical electric apparatus for aircraft

All units are approximately to same scale

Range of Aircraft Electric Apparatus Being Tested

Auxiliaries for aircraft range from totally-enclosed d-c computer motors with approximately one-watt shaft output to pressure-ventilated 40-kva 400-cycle alternators[4] with built-in exciter as shown in Figure 4. In between, there are inverters, dynamotors, geared motors, motor generator sets, and Amplidynes. Some have compensating windings and interpoles—others do not—but each has different brush problems as indicated in Figure 5. Five different brush grades from four separate carbon manufacturers are used on the four machines in Figure 4.

Totally enclosed and self-ventilated machines are chamber tested at low ambients, but pressure-ventilated generators (located in the engine cell) may have outside surfaces exposed to air 100 degrees centigrade warmer than that forced through the inside. The mass of cooling air per minute passing through a machine decreases directly as the air density with self-ventilation, but approximately as the square root with rammed-air (pressure) ventilation—and at 40,000 feet the ventilation is reduced, respectively, to one-quarter and one-half compared to sea level. Self-ventilated machines are more likely to overheat when specification altitudes are exceeded. To obtain representative temperature rises with simulated tests, the air pressure should be accurately controlled.

The National Advisory Committee for Aeronautics standard tables[5] indicate an isothermal atmosphere of −55 degrees centigrade at all altitudes above 35,332 feet. Smithsonian tables[6] show a minimum temperature of −83 degrees centigrade at 55,000 feet above the equator. Temperatures lower than −55 degrees centigrade are provided for some of the test chambers described below.

Test Chambers for Self-Ventilated and Totally Enclosed Apparatus

Two altitude chambers similar to that in Figure 6 have been used for approximately 600 tests[7] on small apparatus, beginning in September 1941. A single-stage Freon-12 refrigeration system with a $7\frac{1}{2}$-horsepower reciprocating compressor gives the following approximate temperatures at indicated conditions in the chamber shown:

Watts Dissipated Inside Chamber	Simulated Altitude (Ft)	Internal Ambient Temp, C
0	40,000	−55
400	40,000	−50
1,100	35,000	−40
2,000	35,000	−30

These chambers will operate at 60,000 feet with reduced thermal capacity. Minimum refrigerating-coil temperature is approximately −58 degrees centigrade. Air is circulated through this coil with a fan. Desired air pressures are maintained with vacuum pumps.

To minimize contamination, all thermal insulation was put on the outside of chambers. The smooth inside surfaces of fabricated steel are washed after each test. Clean, fresh air is drawn from a controlled-atmosphere room, passed through the dryer of Figure 7, expanded through a throttle valve into the chamber, and pumped out continuously at a rate corresponding to five to ten complete changes per hour to prevent accumulation of organic vapors.

Dew points of from −60 to −70 degrees centigrade can be obtained with the alumina dryer. A low dew point within the chamber is further assured by the cold surfaces of the refrigerating coils. By admitting dry air, the formation of ice on the evaporator is avoided, and continued efficiency of the refrigerating system is maintained. Inside air is withdrawn through a pipe from a point near the test machine for periodic checks with a dew-point potentiometer to determine moisture content.

A laminated glass window in the chamber permits observation. Brush wear is indicated by special gauges mounted on test machines. Electrical connections and thermocouple leads are brought out through a sealed terminal board. If two machines must be coupled together both are placed inside. This avoids the difficulties of a shaft seal through the cham-

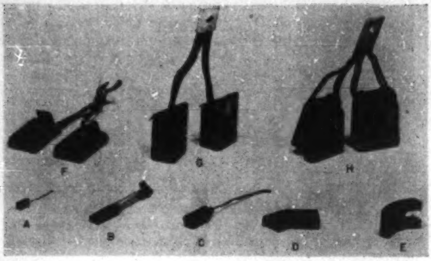


Figure 5. Carbon brushes for aircraft

A—One-eighth-inch-square size for motor of Figure 4A

B, C—With tamped-in shunts for cartridge holders

D, E—Without shunts for pivoted holders

F—Thirty-degree reaction type with riveted shunts

G—Radial with 30-degree top angle for box holders

H—Pair of laminated brushes for aircraft generator of Figure 4C

ber wall. The double door at the end has an inner steel section for strength, and an outer portion with fiber-glass insulation.

A third chamber of similar size has a multistage Freon-22 refrigerating system to get temperatures as low as −75 degrees centigrade. Its evaporator consists of steel pipes welded to the inside of chamber walls. A very low dew point is assured by such cold surfaces. This chamber is adapted to general investigations of breakaway torque of grease-lubricated bearings, insulation strength, or brush and commutator wear under conditions as severe as anything reported in flight tests to date.

The above altitude chambers and their auxiliaries may be operated 24 hours a day, and continuous runs of 50 hours or more have been made.

Facilities for Testing Pressure-Ventilated Machines

Higher-output units such as the generators[8] of Figure 4 are usually blast ventilated with air pressures of six to ten inches of water. Most generators must be tested over a two-to-one speed range and must operate in parallel. Sometimes a generator and motor need to be tested together. Figure 8 shows a schematic arrangement of two altitude chambers which are being used to test units as large as 40-kva rating. These chambers may be operated together or independently. An interior view of chamber number 2 is shown in Figure 9.

Additional features of this equipment are:

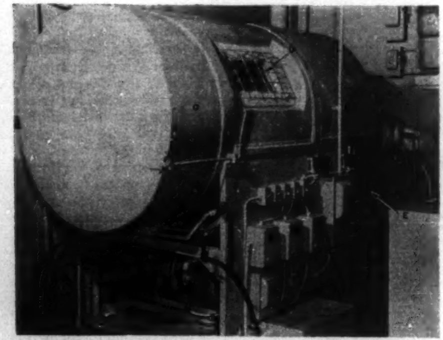
1. The steam ejectors, dampers, and turboblower to control air flow and pressure.
2. Filters and dryers to control air composition.
3. A multiple refrigerating system designed for 20 pounds of air per minute at 50,000 feet altitude conditions.
4. The dual chambers and mechanical drives suitable for testing machines as large as 75 kva.

CONTROL OF AIR FLOW AND PRESSURE

The system is designed for "purged" air, recirculation, or a combination of the two, and is controlled from the panels in Figure 10.

For "purged" operation, outdoor air is drawn through the filters, dryers, and refrigerating coils, and passes through the apparatus under test into one of the chambers from which it is exhausted with steam ejectors. One or both ejectors may be used on either chamber. Each ejector is designed for 5,800 pounds of steam per hour and 10 pounds of air per minute with a back pressure of three inches of mercury absolute, which corresponds to 53,000 feet altitude. Either chamber may be thus evacuated in less than one minute. Chamber pressures are controlled by motor-operated dampers which bleed air into the exhaust lines. Air flow through test apparatus is measured with Pitot tubes and controlled by motor-operated dampers located in the six-inch air supply lines near low-temperature refrigerating coils.

To operate "recirculating," the air inlet is closed, the alumina dryers are bypassed, low pressure is maintained with the ejectors, and air is recirculated with a



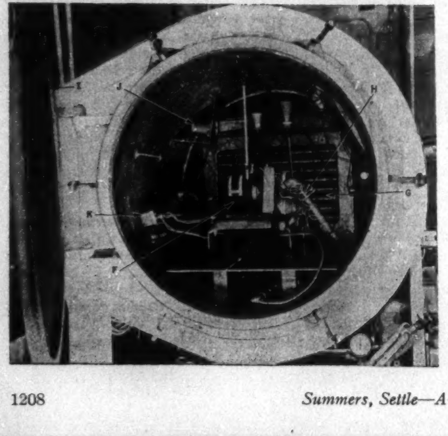


Figure 6. Exterior (upper) and interior (lower) of altitude chamber for testing self-ventilated and totally enclosed machines

Chamber inside dimensions—three feet diameter by five feet long

C—Double door with insulation outside

D—Observation window

E—Dew-point potentiometer

F—Motor ready for test

G—Refrigerating coil

H—Pipe for sampling air near motor

I—Steel door (inside)

J—Altimeter

K—Vacuum-sealed terminals through wall

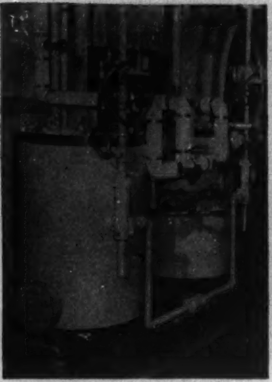


Figure 7. Alumina dryer through which air passes before entering test chambers

One tower may be reactivated while the other is used

blower (pipes shown dotted in Figure 8) through the refrigerating system and test apparatus. Pressure across the test machine is then controlled by the speed of the blower which is a steam-driven turbosupercharger suitable for speeds up to 20,000 rpm. The blower discharge passes over a water-cooling coil to reduce the temperature of air before it reaches the refrigerating coils. Recirculation has the advantage of bringing less water into the system. By bleeding fresh air through the inlet (valve 15), a combination of purging and recirculation can be obtained.

The turboblower can supply air at sea level to one chamber while the other is operating at high-altitude conditions. The change from altitude to sea level in the blower circuit is accomplished by valves 4 to 17 inclusive in Figure 8.

Prevention of Contamination

Outside air enters the system through a filter that removes solid particles. The air next passes through canisters of activated carbon which adsorb contaminants such as sulphur dioxide, hydrogen sulfide, ammonia, and organic vapors. Then the air goes through another filter that prevents carbon particles from being carried away. Stainless-steel air pipes are used to avoid paint on the inside. All thermal insulation is on the outside. Dry inorganic material is substituted for conventional packing around the stems of air valves. The air does not come into contact with anything but clean, hard surfaces from the time it leaves the filter until it enters the apparatus being tested.

Control of Humidity

If exposed to wet air, low-temperature refrigerating coils become coated with ice which reduces Btu absorption and restricts air flow. To dry 20 pounds of air per minute (at sea-level pressure) from 32 degrees centigrade and 85 per cent relative humidity to −50 degrees centigrade dew point, almost 700 pounds of water must be removed every 24 hours. Incoming air passes through the preliminary refrigerating unit 1 (Figure 8) which is held at approximately three degrees centigrade so that part of the moisture may be condensed as water instead of ice and drained from the system. About 80 per cent of total moisture would be removed at this point if initial conditions were as above.

The air next passes through alumina dryers similar to Figure 7 where its dew point is reduced to −50 degrees centigrade or lower, which corresponds to less than 0.02 grain per cubic foot. Air leaving this dryer would have over 99.8 per cent of the aformentioned initial moisture removed.

Figure 9. Interior of altitude chamber number 2 for testing pressure-ventilated machines

Each chamber is six feet long by six feet in diameter. Panel for electric system is at the left

A—40-kva generator ready for test
B—Refrigerated-air-supply tube
C—Mounting plate
D—Inside terminal board
E—Rubber seal for vacuum-tight door

As the air passes on through the refrigerating system, some ice may be deposited on the coldest unit 3*A* or 3*B*. If the dew point were reduced here from −50 to −65 degrees centigrade, it would take over 30 hours to deposit one pound of ice with the maximum flow of 20

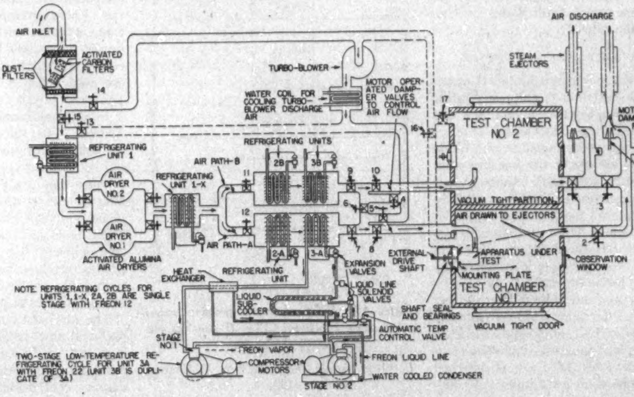

AIR INLET
DUST FILTERS
ACTIVATED CARBON FILTERS
14
15
13
TURBO-BLOWER
WATER COIL FOR COOLING TURBO-BLOWER DISCHARGE AIR
MOTOR OPERATED DAMPER VALVES TO CONTROL AIR FLOW
17
16
REFRIGERATING UNIT 1
REFRIGERATING UNITS
2B
3B
AIR PATH-B
REFRIGERATING UNIT 1-X
AIR DRYER NO. 2
AIR DRYER NO. 1
11
12
9
10
6
5
4
7
8
AIR PATH-A
2-A
3-A
ACTIVATED ALUMINA AIR DRYERS
HEAT EXCHANGER
REFRIGERATING UNIT
EXPANSION VALVES
LIQUID LINE SOLENOID VALVES
LIQUID SUB-COOLER
NOTE: REFRIGERATING CYCLES FOR UNITS 1, 1-X, 2A, 2B ARE SINGLE STAGE WITH FREON 12
STAGE NO. 1
FREON VAPOR
TWO-STAGE LOW-TEMPERATURE REFRIGERATING CYCLE FOR UNIT 3A WITH FREON 22 (UNIT 3B IS DUPLICATE OF 3A)
COMPRESSOR MOTORS
STAGE NO 2
AUTOMATIC TEMP CONTROL VALVE
FREON LIQUID LINE
WATER COOLED CONDENSER
SHAFT SEAL AND BEARINGS
EXTERNAL DRIVE SHAFT
AIR DISCHARGE
STEAM EJECTORS
TEST CHAMBER NO. 2
MOTOR DRIVEN DAMPER VALVE FOR ALTITUDE CONTROL
STEAM NOZZLE
VACUUM TIGHT PARTITION
AIR DRAWN TO EJECTORS
3
2
APPARATUS UNDER TEST
OBSERVATION WINDOW
MOUNTING PLATE
TEST CHAMBER NO. 1
VACUUM TIGHT DOOR

Figure 8. Air-conditioning and refrigerating system for testing pressure-ventilated apparatus

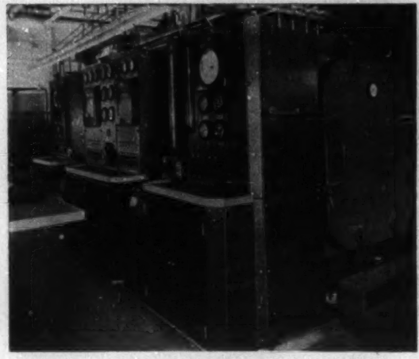


Figure 10. Instruments and control panels for air flow and refrigerating system

Chamber number 2, with altimeter in closed door, is shown at right

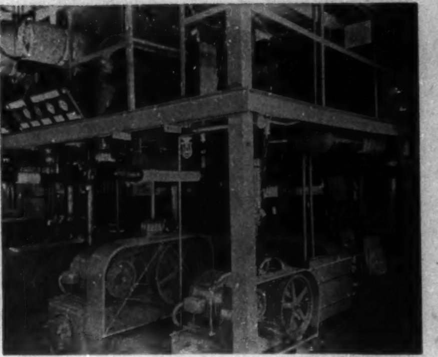


Figure 11. Refrigerating equipment for system of Figure 8

The six cooling coils are in enclosures on platform above the eight motor-driven compressors. Dial gauges at upper left indicate pressures of Freon-12 and Freon-22 in different parts of the system

pounds of air per minute. Continuous runs of 50 hours at stable temperatures have been made without noticeable ice formation or loss in refrigerating efficiency of the coils.

Final dew point depends on which dampers (before refrigerating step 2*A* or after 3*A*) are used to throttle the air from atmospheric to chamber pressure. Relative humidity changes inversely with volume as air expands at constant temperature, and when throttling to 40,000-feet conditions, the decrease in moisture per cubic foot would be about five to one. Dew points of −80 to −85 degrees centigrade can be obtained with the −70-degree-centigrade coils if air expansion occurs thereafter. By reheating the low-dew-point air before it reaches test apparatus, the air temperature and humidity can be controlled independently.

MULTIPLE REFRIGERATING SYSTEM

The inlet air (Figures 8 and 11) passes through at least four of the refrigerating units in series before reaching the test machine, as indicated in Table I.

Unit 1 condenses moisture out of incoming air just above the freezing point as previously explained. Unit 1*X* removes that portion of the "heat of moisture adsorption" that is added to the air by the alumina dryer and also further lowers the air temperature. Part of this heat of adsorption is removed by water coils inside dryers.

Air leaving unit 1*X* follows either path *A* or *B* for additional refrigeration by units 2*A* and 3*A*, or 2*B* and 3*B*. Path *B* is a complete duplicate (including compressors) of path *A*. Each is designed for 20 pounds of air per minute at 50,000-foot conditions. This multiple system is provided so that

1. Refrigeration for each test chamber can be independently controlled.
2. The evaporators in one path can be defrosted while the others are in use, thereby assuring long continuous runs by switching from one path to the other.
3. Tests can be continued while a unit is serviced.

Air may be sent to chamber number 1 or number 2 through either path *A* or *B* by adjusting the valves. Both chambers can be paralleled on one refrigeration path, and vice versa.

The refrigerating units 1, 1*X*, 2*A*, and 2*B* are conventional, single-stage, using Freon-12. The lower-temperature units 3*A* and 3*B* are two-stage, Freon-22, with 20- and 10-horsepower compressors in series. Figure 12 shows that Freon-22 has about 65 per cent higher vapor pressure than Freon-12. At −70 degrees centigrade their approximate absolute pressures are respectively 6 and 3.6 inches of mercury. The higher pressure[9] of Freon-22 improves the volumetric efficiency and Btu rating of compressors because of the greater mass of gas drawn into the cylinders during each intake stroke. For very low temperatures, a refrigerant with lower boiling point is desirable to avoid the inefficient compression of extremely rarefied gas.

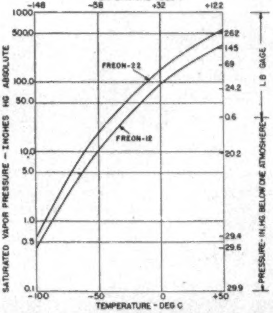


Figure 12. Saturated vapor pressure versus temperature curves for CCl_2F_2 (Freon-12) and $CHClF_2$ (Freon-22)

Data from Kinetic Chemicals, Inc., Wilmington, Del.

There is approximately a four-to-one saving in motors and compressors (total of ratings) by refrigerating air in successive steps, instead of using a single evaporator to remove all heat energy at the lowest temperature level. Note that 30 horsepower of motors are required for 14,000 Btu absorption in the coldest unit, whereas only 3 horsepower are needed for 28,000 Btu in units 1 or 1*X*.

Fiber-glass insulation is used on the air system. The thickness is eight inches on colder portions. No insulation was put on the two apparatus test chambers, because they are kept warm to simulate aircraft-engine cells. This also avoids moisture condensation inside chambers when doors are opened after a test. If cold ambient temperatures are desired, the test machine may be surrounded by a temporary enclosure into which refrigerated air can be passed.

The air passes through only ten feet of steel pipe between the coldest coil and test apparatus. Starting with all parts at room temperature, air at −55 degrees centigrade can be obtained in

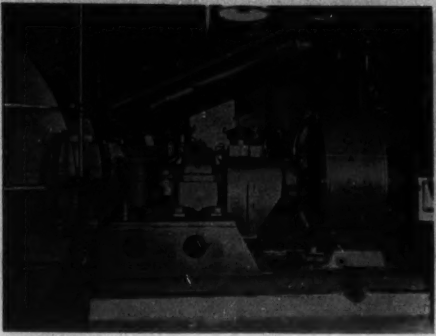


Figure 13 (above). One-hundred-horsepower d-c motor (*A*) and pressure-lubricated overdrive gear unit (*B*) for testing aircraft apparatus in chamber number 2 at speeds up to 10,000 rpm

Oil reservoir is indicated by *C*

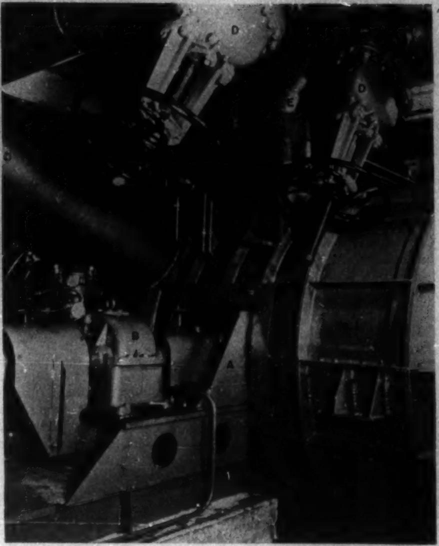


Figure 14 (right). Air lines and mechanical drive for chamber number 1

A—Mounting plate assembly
B—Gear unit
C—Refrigerated air line
D—Gate valves in 12-inch diameter exhaust air lines (*E*) to steam ejectors

approximately $1\frac{1}{2}$ hours. If evaporators are cold from previous operation, this may be reduced to ten minutes, depending on length of shutdown. After passing the coldest coil, the air can be reheated to any desired temperature before reaching the test apparatus. To defrost evaporators, the refrigerating equipment is stopped and warm dry air from the alumina dryer is drawn through them by the steam ejectors.

MECHANICAL AND ELECTRICAL FEATURES

The two test chambers (Figures 8, 9, and 14) were made from a fabricated steel tank 6 feet in diameter and 14 feet long. An air tight partition divides the space equally between them. Ribs were welded on the outside for strength, and a vacuum-tight door of size $3\frac{1}{2}$ by 5 feet was put on each end. Sealed glass windows permit observation of machines inside.

Test machines are supported by mounting plates inside the chambers and are driven by d-c motors located outside. Individual plates, with rabbet fits to match those of different test machines, may be bolted to the "master" alignment plate, and one size of driving spline may be substituted for another. Each mounting plate assembly, which also contains the shaft seal, extends through the chamber wall and is rigidly fastened to the same outside base that supports the corresponding driving motor and gear unit. An isolation clearance of approximately $\frac{1}{16}$ inch between the plate and chamber (Figure 15) is flexibly sealed to accommodate differential thermal expansion and to prevent transmission of vibrations or stresses that might disturb shaft alignment.

The shaft seal is operated without oil to minimize contamination. This seal does not overheat at high speeds, because there are no rubbing parts. A radial clearance of approximately 0.003 inch between shaft labyrinth and babbitt sleeve permits slight leakage of air when pressure inside the chamber is reduced, but this leakage can be discharged by the steam ejectors without passing through the machine being tested. Several hundred hours of running at 6,000 to 8,000 rpm indicate this method of sealing to be satisfactory.

A 100-horsepower d-c motor (Figure 13) is used with one chamber and a 75-horsepower motor with the other. Both motors are rated 1,750 to 600 rpm at 250 volts, and 600 to 310 rpm at 125 volts on the armature with corresponding reduction in horsepower. The power for them is obtained from a three-unit set consisting of an induction motor and two 125-volt 50-kw d-c generators which may be connected either in series or parallel to obtain this speed control. An overdrive gear with ratio of 5.72 permits operation from 1,800 to 10,000 rpm with either direction of power flow for the testing of aircraft generators and supercharger motors.

The main driving motors are controlled from the panel shown in Figure 9. This panel also has a d-c and an a-c section for each altitude chamber. Two aircraft machines may be operated in parallel, in pumpback, or independently. A d-c unit may be tested in one chamber while an a-c unit is in the other. Motor-operated load rheostats rated 800 am-

Table I

Refrigerating Unit	Absorption Rating in Btu Per Hour	Approximate Coil Temp, C	Refrigerant	Horsepower of Driving Motors
1	28,000	+ 3	Freon-12	3
1X	28,000	−10	Freon-12	3
2*A*	20,000	−40	Freon-12	15
3*A*	14,000	−70	*Freon-22	20 and 10
Subtotal	90,000	(Air path *A* only)		51
Alternative air path				
2*B*	20,000	−40	Freon-12	15
3*B*	14,000	−70	*Freon-22	20 and 10
Totals	124,000	(Air paths *A* and *B* combined)		96

*Two-stage units.

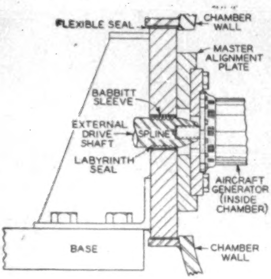


Figure 15. Assembly of drive shaft and seals through altitude-chamber wall

Additional views of generator are shown in Figures 4D and 9

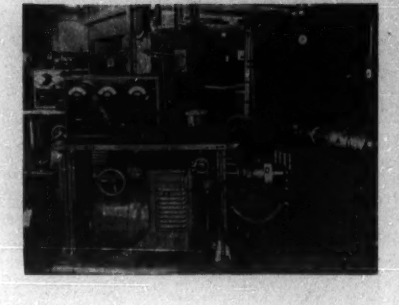


Figure 16. Portion of aircraft-apparatus-development-test laboratory

A and *B* are nine-kilowatt and six-kilowatt generators coupled together for pumpback tests on portable stand *C* which may be moved into altitude chamber. Generator *D* is mounted on variable-speed drive *E* for sea-level tests

peres are provided for 30-volt d-c generators. For a-c loads of more than 60 kw, a three-phase water box is paralleled with three rheostats in wye. Each chamber has 50 lines in sizes from 25 to 1,000 amperes, and 24 pairs of copper–copnic thermocouples. The a-c lines are rated 230 volts, 500 amperes, three phase. Insulated, vacuum-sealed leads bring the circuits through chamber walls to inside terminal boards shown in Figure 9. Automatic temperature recorders are used with the thermocouples.

This equipment is flexible and convenient. A test at 50,000 feet altitude and −55 degrees centigrade can be interrupted, atmospheric pressure restored, a minor adjustment made on test unit, the chamber closed and evacuated, and the test resumed in less than ten minutes.

Importance of Sea-Level Tests

Aircraft fly at low as well as high altitudes. The stratosphere has been emphasized in recent months, but sea-level tests are also important when developing new materials or appraising old ones for aircraft. New brush grades and other parts that have unacceptable performance on the ground can be eliminated without complex altitude-chamber tests. Initial investigations of new materials are usually made in ordinary room atmosphere. Pumpback tests, used for some of the larger machines, avoid individual driving motors and load rheostats. Two machines *A* and *B* are run simultaneously with less space and auxiliary equipment than the one generator (*D*) shown in Figure 16. The portable stand permits units to be moved in and out of the altitude chamber quickly without disturbing the mechanical setup.

Another stand permits parallel operation of ten units at 10,000 rpm to obtain data on bearings, commutators, brush holders, and other parts at no load. By adjusting ventilation and using individual heating units around bearing housings, temperatures are controlled independently of load. Any machine selected for special tests is usually first run 24 hours or longer on this stand at maximum speed to check for shifting of commutator bars or other defects which might disqualify it for the purpose in mind.

Other typical sea-level tests are the parallel operation of generators with voltage regulators, vibration response of mounting flanges and shafts, ventilation, heating, and insulation life. Such tests, some of 500 to 1,000 hours duration, have indicated which materials should be investigated further at high altitudes.

Conclusions

Designers of aircraft auxiliaries need a quick, efficient substitute for flight tests. Appropriate altitude chambers are serving this purpose, and are hastening the development of improved electric apparatus. Agreement between flight and factory tests on carbon brushes indicates that stratosphere conditions can be simulated effectively in clean, ventilated chambers with controlled humidity, temperature, and air pressure. Continued experience with altitude chambers should greatly reduce the amount of flight testing required to develop new equipment for aircraft.

Electric apparatus and carbon brushes must be tested together. The performance of each part depends on the condition of the others. Brush testing is an overall job that requires adequate facilities for operating complete machines under service conditions. No single brush grade, or bar alloy, looms as an over-all solution to commutation problems. Brushes cannot be appraised fully for one machine by testing them on another of widely different design. There is yet no substitute for the mating of specific brush grades to individual machine designs on the basis of experimental evidence.

Two years ago some aircraft auxiliaries had less than five hours brush life above 30,000 feet. The indicated high-altitude life has been extended to 50, 200, and on some units to more than 500 hours. The life at sea level has also been improved. But the desired performance is not yet achieved on all aircraft machines. Some of the new brushes depend largely on the prior formation of a commutator film at low altitude, and high-altitude wear may begin abruptly if the initial film is dissipated. The ideal brush should form and maintain a wear-resistant film on a freshly machined commutator at any altitude. Much remains to be done, but progress is being made by design changes, development of new materials, improved testing methods, and a better understanding of current-collection phenomena. These advances have been speeded by co-operation between the Army Air Forces, National Defense Research Committee, carbon brush companies, electric-machinery manufacturers, and others.

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Impedance of 400-Cycle Three-Phase Power Circuits on Large Aircraft and Its Application to Fault-Current Calculations

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THE use of 200-volt three-phase 400-cycle a-c systems in large aircraft, with multiple circuits fused for fault clearing of sections, requires an accurate analysis of fault currents at all points of the system. A preliminary investigation of impedance data for 400-cycle aircraft circuits disclosed that basic data were available for certain types of impedance, but that certain impedance values required for line-to-ground faults were completely lacking. The object of this report is to complete the impedance data and to present in one complete compilation all impedance data required for analysis of faults in 400-cycle grounded-neutral aircraft systems as now proposed.

Power-transmission-line theory provides methods and data for fault-current calculations at conventional frequencies. It appears that the methods are adaptable to a-c power systems in large aircraft, but that data for application of these methods must be obtained analytically and verified by test.

Basic Data

The theory of symmetrical components provides a logical method of attack on this problem. A three-phase system requires three sets of impedance components, known as positive sequence, negative sequence, and zero sequence, which are combined in various ways to represent the several types of faults which can occur. These impedance values for the alternators and motors have been supplied by the manufacturers' design and test engineers. The positive- and negative-sequence-impedance values are equal for static equipment, including distribution circuits. The zero-sequence impedance is different from the other impedance values on distribution circuits and must be obtained individually for each combination of wire size and spacing. The method of calculating zero-sequence impedance for aircraft distribution circuits will be discussed later.

The basic nomenclature and formulas for fault-current calculations are as follows:

E_{L-L} = line-to-line volts
E_{L-g} = line-to-ground volts
I_f = fault current
Z_1 = positive-sequence impedance
Z_2 = negative-sequence impedance
Z_0 = zero-sequence impedance
Z_f = fault impedance

For line-to-ground faults:

$$I_f = \frac{3\ E_{L-g}}{Z_1+Z_2+Z_0+3Z_f}$$

For line-to-line faults:

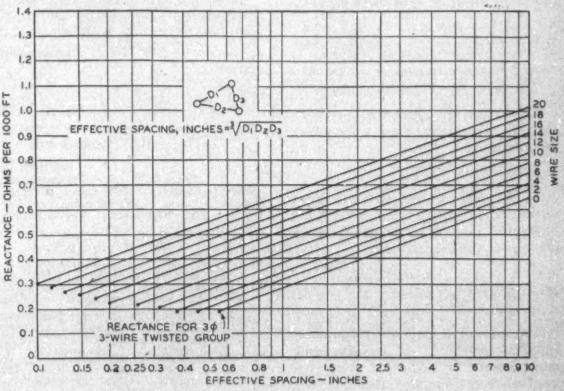
$$I_f = \frac{E_{L-L}}{Z_1+Z_2+Z_f}$$

For three-phase faults:

$$I_f = \frac{E_{L-g}}{Z_1+Z_f}$$

Preliminary measurements indicate that the fault resistance for a copper wire lying on aluminum is approximately 0.007 ohm, and therefore Z_f can be disregarded, as the effect of Z_f on the fault currents will be negligible.

Figure 1. Positive- and negative-sequence reactance of *AN-J-C-48a* wire at 400 cycles



Positive- and Negative-Sequence Impedances

Positive- or negative-sequence reactance for a single wire is calculated by use of the following formula:

$$X = 2\pi f\left(80+741.1\log_{10}\frac{D}{r}\right)10^{-6}$$

where

X = reactance in ohms per mile of single conductor
D = axial spacing in inches between conductors (D is equivalent or geometric mean distance if wires are unsymmetrical)
r = radius of conductor in inches

For metallic single-phase circuit calculations the value of X must be multiplied by twice the length of the circuit and used with line-to-line voltage, but in three-phase calculations X will be multiplied by the length of the line and used with line-to-neutral voltage. A chart showing reactance and resistance in ohms per 1,000 feet is shown in the Westinghouse book on "A-C Electrical Systems for Large Aircraft";[1] also, a table was published in "General Electric Data Folder 63,004."[2] Both are in agreement with the basic formula. A copy of the Westinghouse Electric and Manufacturing Company charts for reactance is shown as Figure 1, and the 400-cycle resistance, taken from "General Electric Data Folder 63,004," is shown as the seventh column of Table I. These two sources provide all the information required for the positive- and negative-sequence impedances.

Zero-Sequence Impedance

The calculation of zero-sequence impedance values for power-transmission and distribution lines has received much consideration since the "Method of Sym-

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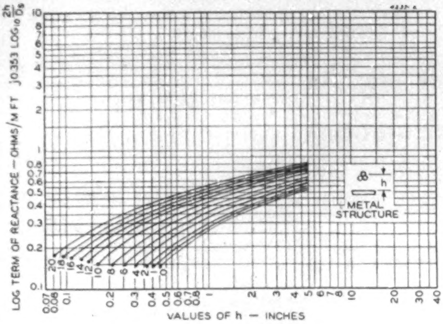


Figure 2. Logarithmic term of zero-sequence reactance formula for *AN-J-C-48a* aircraft wire in metal aircraft adjacent to flat (or approximately flat) surfaces in 400-cycle a-c systems

Calculated on basis of wire sizes from Table I

Z_0 = zero-sequence impedance

r_c = resistance one wire ÷ 3

h = distance to ground return in inches

D_s = group self geometric mean distance in inches

Z_g = correction for return circuit

⊙ = point of minimum h for wire size when adjacent to structure

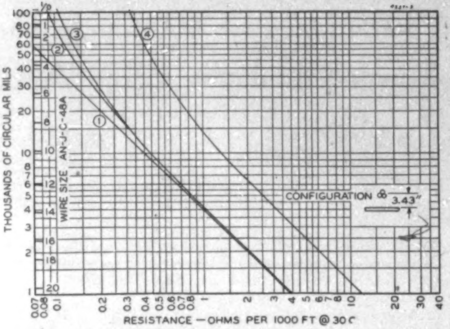


Figure 3. Zero-sequence resistance for *AN-J-C-48a* aircraft wire laced together and transposed and installed adjacent to flat metal surfaces at 400 cycles

① Calculated d-c resistance of three wires in parallel

② Calculated a-c resistance of three wires in parallel plus measured a-c resistance of *B*-19 fuselage

③ Measured a-c resistance of three wires in parallel plus return through fuselage of *B*-19

④ Zero-sequence a-c resistance (curve ③×3)

metrical Components" was published in 1918 by Doctor C. L. Fortescue. A practical formula has been developed which consists of a part derived mathematically and a part obtained from empirical curves which are selected according to the conductivity of the return path, normally the earth.

A common form of this formula for a three-phase line is as follows:[3]
where

$$Z_0=3(r_c+jx_c+Z_g)=3\left(r_c+j0.0007411\ 2\pi f\times \log_{10}\frac{2h}{D_s}+Z\right)$$

Z_0 = zero-sequence impedance in ohms per mile

r_c = resistance of power conductors, in parallel, per mile

h = mean height of wires above ground in inches

$Z_g=r_g+jx_g$

r_g = resistance per mile of earth return

x_g = reactance correction per mile to allow for finite values of earth conductivity

The group self geometric mean distance D_s is obtained by use of the following formula:

$$D_s=\sqrt[9]{d_s^3\times S_1^2\times S_2^2\times S_3^2}$$

S_1, S_2, and S_3 are center-to-center separation in inches between the three conductors, and d_s is the self geometric distance of a single conductor.

$d_s=K\sqrt{A}$

K = a constant (see reference 3)

A = area in circular mils

The value of d_s may be obtained from standard handbooks or from the cable manufacturer or calculated. See page 24 of reference 3 for value of K.

J. E. Clem has published tables and charts in convenient form for dealing with transmission-line problems. Reproductions of the charts for r_g and x_g have been included in reference 3 in the chapter on "Calculation of Fault Currents."

A preliminary investigation of the fault-current problem for aircraft circuits was made, and it was found that no data were available on zero-sequence impedance which could be applied directly to 200-volt three-phase 400-cycle power systems in aircraft. Inasmuch as the desired protective system required close co-ordination at high line-to-ground fault currents, it was decided that reliable values for zero-sequence impedance must be determined.

An investigation of the present method of calculating fault currents was made, and it was decided to apply the method as developed for power transmission systems.

Table I is a tabulation of wire characteristics specified by *AN-J-C-48a*. A seventh column has been added to show a-c resistance at 400 cycles taken from "General Electric Data Folder 63,004."

The zero-sequence-impedance formula for 400-cycle circuits on a 1,000-foot basis is as follows:

$$Z_0=3\left(r_c+j0.353\log_{10}\frac{2h}{D_s}+Z_g\right)$$

ohms per 1,000 feet

where

h = average distance of wires from surface of metal skin or structure in inches

$Z_g=r_g+jx_g$ for aircraft structure (to be found by test)

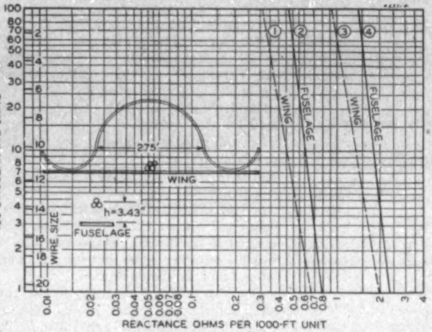
D_s = group geometric mean distance in inches

D_s for three-phase line $=\sqrt[9]{d_s^3\times S_1^2\times S_2^2\times S_3^2}$

Table II was prepared to give the values of $\left(j0.353\log_{10}\frac{2h}{D_s}\right)$ for values of h

Table I

AN-J-C-48a						
Cable Designation	Nominal Conductor Area (Cir Mil)	No of Wires, Min	Max D-C Resistance of Finished Cable (Ohms Per 1,000 Feet at 20 C)	Max Diameter of Stranded Conductor (Inches)	Diameter of Finished Cable (Inches)	General Electric Data Folder 6,3004—A-C Resistance Min Spacing Between Conductor—20 C
A N 20	1,119	7	10.25	0.040	0.100	10.25
A N 18	1,779	7	6.44	0.050	0.115	6.44
A N 16	2,409	19	4.76	0.061	0.130	4.76
A N 14	3,830	19	2.99	0.076	0.150	2.99
A N 12	6,088	19	1.88	0.096	0.170	1.88
A N 10	10,443	37	1.10	0.122	0.200	1.10
A N 8	16,864	133	0.70	0.167	0.255	0.70
A N 6	26,813	133	0.436	0.218	0.310	0.440
A N 4	42,613	133	0.274	0.272	0.370	0.282
A N 2	66,832	663	0.179	0.345	0.445	0.188
A N 1	81,807	812	0.146	0.384	0.495	0.158
A N 0	104,118	1,033	0.114	0.432	0.550	0.127



① Measured reactance three wires in parallel, return in wing

② Measured reactance three wires in parallel, return in fuselage

③ Zero-sequence reactance of three-phase circuit in wing of *B*-19 (curve ①×3)

④ Zero-sequence reactance of three-phase circuit in fuselage of *B*-19 (curve ② × 3)

Figure 4. Zero-sequence reactance for *AN-J-C-48a* aircraft wire laced together, transposed and installed as per drawings in *B*-19

Calculated values for curve 2 using $j0.353 \log_{10}\frac{2h}{D_s}$ from zero-sequence-impedance formula are so close to test values that a separate curve cannot be drawn

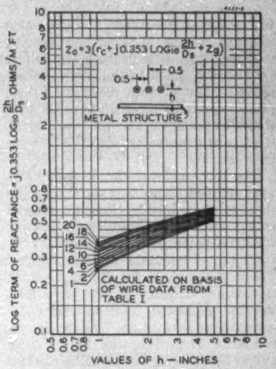


Figure 6. Logarithmic term of zero-sequence-reactance formula for *AN-J-C-48a* aircraft wire in metal aircraft adjacent to flat (or approximately flat) surfaces in 400-cycle a-c systems

Calculated on basis of wire data from Table I

Z_0 = zero sequence, ohms per 1,000 feet

r_c = resistance one wire ÷ 3

h = distance to ground return structure in inches

D_s = group self geometric mean distance in inches

Z_g = correction for return path (see Z_g curves)

from minimum to five inches. The data from Table II were used to make Figure 2 which is more convenient for use where h is not in multiples of 0.5 inch.

In addition to the foregoing data it was necessary to determine the value of Z_g. A test project on a large bomber was planned and carried out with precision laboratory instruments to obtain test data on 400-cycle power circuits in large aircraft. These data will be used to verify the calculations and provide the empirical data for r_g and x_g.

The Detroit Edison Company provided the laboratory instruments and two engineers from their laboratory for the operation of the instruments. All d-c resistance measurements were obtained by use of a General Electric double bridge; a-c resistance and reactance measurements were made by use of a special a-c bridge designed for these tests. All pieces of equipment used were carefully calibrated in the Detroit Edison laboratory before being used for the tests. See Figure 8 for details of test circuit.

The oscillator was adjusted to 400-cycle output and checked at frequent intervals to make certain that the frequency held constant at 400 cycles. The harmonic analyzer was set at 400 cycles and adjusted to amplify and measure any small 400-cycle voltage differential between points *a* and *b*. The variables *C*, *Q*, and *S* were adjusted to obtain minimum deflection on the instrument of the harmonic analyzer for each test. The equations for the unknown *P* and *L* are as listed below:

$$P=\frac{QR}{S}$$

$$L=CQR$$

where

P = resistance

and

L = inductance

Since the circuit design proposed for aircraft provides for grounding the neu-

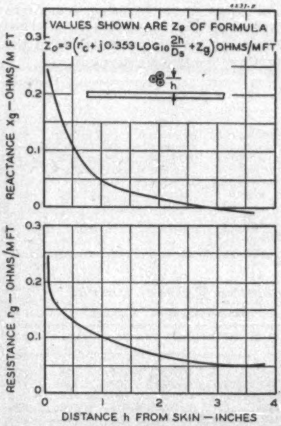
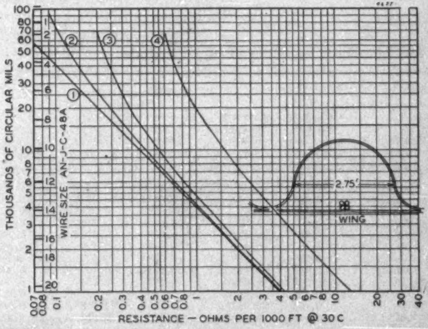


Figure 5. Impedance of return path through Dural sheet inductively coupled with three-phase wires

Thickness of Dural = 0.030 inch

Figure 7. Zero-sequence resistance for *AN-J-C-48a* aircraft wire laced together and transposed and installed in a wing of a large airplane as per sketch—400-cycle alternating current



① Calculated d-c resistance of three wires in parallel

② Calculated a-c resistance of three wires in parallel plus measured a-c resistance of *B*-19 wing

③ Measured a-c resistance of three wires in parallel plus return through fuselage of *B*-19

④ Zero-sequence a-c resistance (curve ③ × 3) tral of the alternators to the aluminum structure of the airplane and running only the phase wires to the load, the first tests were planned to simulate this condition. Test samples, made up of three phase wires laced together, were installed through openings in the bulkheads of the fuselage at a mean distance of 3.43 inches from the skin.

The zero-sequence-circuit conditions were satisfied by bonding the three wires together at both ends, clamping one end to the aircraft structure, and applying 400-cycle potential at the other end between the wires and the aircraft structure. This test sample is the P and L of the test-circuit diagram shown previously. Results calculated from test data for several wire sizes in the fuselage are recorded in Table III. See Figure 3 for curves of resistance values in the fuselage and Figure 4 for curves of reactance values.

Several additional tests were made using a fourth wire for the neutral, with the ground fault to the neutral wire but not involving the aircraft structure. The results for the fuselage wiring are shown in Table IV. Comparison with the three-wire data indicates a large increase in resistance and a slight increase in reactance.

Tying the neutral wire in with the aircraft structure causes a slight increase in the resistance for the smaller wire sizes and slight decrease in the reactance as compared with Table III. These data are shown in Table V for the fuselage.

An analysis of Tables III, IV, and V indicates that a ground return through the airplane structure is satisfactory, and no appreciable gain is secured by using a neutral wire in parallel with the airplane structure. However, return through a fourth wire is satisfactory if the line-to-ground fault currents are too large or if faults are too frequent with the return through the airplane structure.

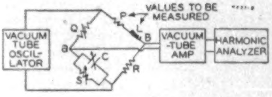


Figure 8. 400-cycle test circuit

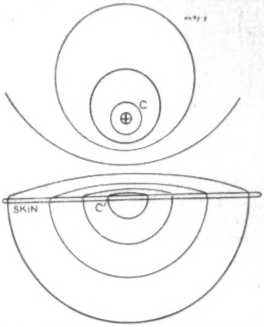


Figure 9. Probable magnetic field of distribution line in aircraft with return through structure

A complete tabulation of test results and calculated values of zero-sequence impedance for the phase wires with ground return through the airplane structure are recorded in Table VI. Column 3 is the calculated r_c at 30 degrees centigrade, and column 4 is the test r_c at 30 degrees centigrade. The difference r_g at 30 degrees centigrade, is 0.0425 ohm per 1,000 feet or 0.0408 ohm per 1,000 feet at 20 degrees centigrade. These values are reasonable as they check, within probable limits of accuracy, with the measured value of 0.051 ohm per 1,000 feet for d-c resistance of the fuselage. The a-c resistance should not be appreciably different from the d-c resistance, and both values should be very low.

Column 6 is the calculated value of j-0.353 $\log_{10} 2h/D_s$ when $h=3.43$ inches and column 7 shows the test values. The average of the differences gives a value of -0.007 ohm per 1,000 feet for x_g. This value probably should be zero.

For the fuselage we may accept the fact, subject to further proof, that, r_g equals 0.0408 ohm per 1,000 feet at 20 degrees centigrade, and x_g is negligible, for the condition where h equals 3.43 inches. The appendix of this report is an analytical method of calculating zero-sequence impedance of a three-phase circuit with return through a flat metal structure. Figure 5 shows the calculated values of r_g and x_g from h equals 0.1 inch to h equals 3.5 inches. If these curves are checked at h equals 3.43 inches, it will be found that $x_g=-0.006$ ohm per 1,000 feet and $r_g=0.05$ ohm per 1,000 feet. This agrees very closely with the test results. For further calculations Figure 5 will be accepted as satisfactory values of r_g and x_g for use at this time in the zero-sequence-impedance formula. However, it is felt that additional zero-sequence-impedance tests at lower values of h should be made before these results are accepted as conclusive.

A typical requirement in aircraft use is to determine zero-sequence impedance of three number 10 conductors laced together and placed two inches from the surface of the aircraft structure.

Table II. Zero-Sequence-Impedance Data

$$Z_0=3\left(r_c+j0.353\log_{10}\frac{2h}{D_s}+Z_g\right)\text{ Per 1,000 Feet}$$

r_c = Resistance of One Conductor Per 1,000 Feet ÷ 3

D_s = Group Geometric Mean Distance in Inches

h = Distance From Wire to Ground Return in Inches

$Z_g=r_g+x_g$ (Correction for Return Circuit—See Z_g Curves, Figure 5)

⁂ = Configuration—Three Wires Laced Together and Transposed

Wire Size AN	400-Cycle Resistance Ohms Per 1,000 Feet		Self Geometric Mean Distance		Min h, Inch AN-J-C-48a	Values of 0.353 $\log_{10}\frac{2h}{D_s}$ Ohms Per 1,000 Feet for h = Min to 5"							
	Conductor	r_c	One Conductor d_s	Group D_s		Min	0.5"	1.0"	1.5"	2.0"	3.0"	4.0"	5.0"
20	10.25	3.42	0.0138	0.0517	0.082	0.178	0.454	0.561	0.623	0.668	0.730	0.775	0.807
18	6.44	2.15	0.0174	0.0613	0.095	0.174	0.428	0.535	0.597	0.642	0.703	0.749	0.781
16	4.76	1.59	0.0213	0.0711	0.107	0.169	0.405	0.512	0.574	0.619	0.680	0.726	0.758
14	2.99	1.00	0.0268	0.0845	0.124	0.165	0.379	0.486	0.547	0.593	0.654	0.700	0.732
12	1.88	0.627	0.0338	0.0992	0.140	0.159	0.355	0.462	0.523	0.569	0.630	0.676	0.708
10	1.10	0.367	0.0452	0.122	0.165	0.152	0.322	0.429	0.490	0.536	0.596	0.643	0.675
8	0.70	0.233	0.0582	0.156	0.210	0.151	0.284	0.391	0.454	0.498	0.560	0.605	0.637
6	0.440	0.147	0.0734	0.192	0.256	0.150	0.253	0.360	0.422	0.467	0.528	0.574	0.606
4	0.282	0.094	0.0925	0.233	0.305	0.148	0.223	0.330	0.392	0.437	0.499	0.544	0.576
2	0.188	0.0627	0.1159	0.284	0.367	0.146	0.193	0.300	0.362	0.407	0.468	0.514	0.546
1	0.158	0.0527	0.1237	0.316	0.408	0.146	0.177	0.284	0.348	0.391	0.455	0.498	0.530
1/0	0.127	0.0423	0.145	0.353	0.454	0.145	0.160	0.267	0.328	0.374	0.435	0.481	0.513

The zero-sequence impedance can be obtained from the figures and tables as follows:

From Table I, *AN* 10 has an a-c resistance of 1.10 ohms per 1,000 feet. r_c equals 1.10 divided by 3 equals 0.367 ohm. The value of $j0.353 \log 2h/D_s$ from Figure 2 is 0.535 ohm. From Figure 5, r_g at two inches is 0.066 ohm per 1,000 feet and x_g is 0.017 ohm per 1,000 feet.

$$Z_0=3(r_c+j0.353 \log 2h/D_s+r_g+x_g)$$
$$Z_0=3(0.367+j0.535+0.066+j0.017)$$
$$Z_0=3(0.433 + j0.552)$$
$$Z_0=1.299+j1.656 \text{ ohms per 1,000 feet}$$

If in the foregoing example the wires were spaced on one-half-inch centers in flat configuration instead of being laced together, a problem would be presented which cannot be solved from the charts but requires new calculations as follows or the preparation of new tables:

$$D_s=\sqrt[9]{(0.0452)^3\times 0.5^4\times 1.0^2}$$
$$D_s=0.2618$$
$$Z_0=3(r_c+j0.353 \log [2h/D_s]+r_g+x_g)$$
$$Z_0=3(0.367+j0.353 \log [2\times 2/0.2618]+0.066+j0.017)$$
$$Z_0=3(0.367+j0.418+0.066+j0.017)$$
$$Z_0=1.299+j1.305 \text{ ohms per 1,000 feet}$$

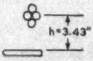
In aircraft wiring we have encountered only three proposed wire separations as follows:

1. Three wires laced together.
2. One-half-inch flat spacing.
3. Two-inch flat spacing.

Of these three, the first two are proposed for aircraft now being designed. Table VII and Figure 6 have been prepared for zero-sequence-impedance values for aircraft wiring with one-half-inch flat spacing. Their use is the same as Table II and Figure 2 for three wires laced together.

Table III. Impedance of Three Conductors in Parallel, Return Through Fuselage—Calculated From Test Data

Wire Size	D-C Resistance (Ohms Per 1,000 Feet) 3 Wires in Parallel and Return (by Test)	400 Cycle, by Test: A-C Resistance (Ohms Per 1,000 Feet) 3 Wires in Parallel and Return	400 Cycle, by Test: Reactance (Ohms Per 1,000 Feet) 3 Wires in Parallel and Return
AN 2	0.0683	0.1290	0.492
AN 4	0.1495	0.204	0.500
AN 6	0.1500	0.210	0.547
AN 8	0.224	0.273	0.560
AN 10	0.408	0.432	0.577
AN 12	0.622	0.681	0.671
AN 14*	1.360	1.420	0.703
AN 16	1.435	1.480	0.678
AN 18			
AN 20	3.650	3.720	0.772



Mean value of h=3.43 inches.
Temperature: Approximately 30 degrees centigrade.

* Measurement of test sample indicated that it was only slightly larger than number 16. Disregard data.

Positive- and negative-sequence-reactance values can be read directly from Figure 1. The three number 10 conductors laced together have a positive-sequence reactance of 0.225 ohm per 1,000 feet. At one-half-inch flat spacing which is 0.63-inch effective spacing the positive-sequence reactance is 0.405 ohm per 1,000 feet.

Impedances When Wire Is Installed in Metal Raceways

It is possible that certain circuits may be installed in raceways formed in fabricated spars, wing stiffeners, and conduits. To simulate this condition, test samples of four conductors laced together were installed between the skin and the corrugated stiffener in the wing of a large airplane. It is reasonable to assume that the positive- and negative-sequence impedances are not affected appreciably by the surrounding metal. The zero-sequence impedance depends to a considerable extent upon the configuration of the return path, however, and should show an appreciable effect due to current distribution in the return path. Tests were conducted in the same manner as for the fuselage, with results as recorded in Table VIII. See Figure 7 for curves of resistance values in wing and Figure 4 for curves of reactance values.

Additional tests with the fourth wire for the neutral, with the ground fault to the neutral wire only, gave results shown in Table IX. Comparison with Table VIII indicates a large increase in resistance and a smaller increase in reactance.

Tying the neutral wire in with the aircraft structure causes a slight increase in resistance for the smaller wire sizes and a slight decrease in reactance as compared with Table VIII. These data are shown in Table X.

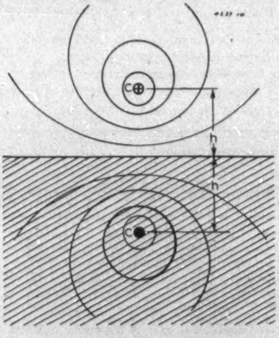


Figure 10. Magnetic field of transmission line with earth return

The case of four *AN*-10 conductors (one intended for insulated neutral but not used) installed in the space between the skin and the stiffener would be computed as follows:

$h=0.970\times 0.200=0.194$ inch
$r_c=$(Table II)$=0.367$ ohm per 1,000 feet at 20 degrees centigrade
$r_g=$(Figure 5)$=0.160$ ohm per 1,000 feet ($h=0.194$)
$j0.353 \log \frac{2h}{D_s}$ (Figure 2)$=0.177$ ohm per 1,000 feet ($h=0.194$)
x_g (Figure 5)$=0.190$ ohm per 1,000 feet ($h=0.194$)

$$Z_0=3\left(r_c+j0.353 \log \frac{2h}{D_s}+r_g+jx_g\right)$$
$$Z_0=3(0.367+j0.177+0.160+j0.190)$$
$$Z_0=1.581+j1.101 \text{ ohms per 1,000 feet}$$

Test data for this installation taken from Table VIII, show $Z_0=1.518+j1.482$.

It appears that the image theory (from the transmission-line formula) does not produce so accurate results for conductors in raceways. The results show a very close agreement in the resistance values as determined by test and by the image theory. The agreement between the reactance values is not too close but may be acceptable for preliminary values. There must be additional test work done to improve the method of calculating

Table IV. Impedance of Three Conductors in Parallel—Calculated From Test Data

Return Through Fourth Wire

Wire Size	D-C Resistance (Ohms Per 1,000 Feet) By Test	400 Cycle, by Test: A-C Resistance (Ohms Per 1,000 Feet)	400 Cycle, by Test: Reactance (Ohms Per 1,000 Feet)
AN 4	0.349	0.381	0.580
AN 10	1.290	1.382	0.651
AN 16	5.680	5.840	0.881

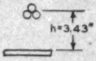


Table V. Return Through Fourth Wire and Fuselage—Calculated From Test Data

Wire Size	D-C Resistance (Ohms Per 1,000 Feet) By Test	400 Cycle, by Test: A-C Resistance (Ohms Per 1,000 Feet)	400 Cycle, by Test: Reactance (Ohms Per 1,000 Feet)
AN 4	0.134	0.1883	0.382
AN 10	0.4015	0.4775	0.520
AN 16	1.433	1.505	0.673

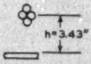


Table VI. Calculated and Test Data on Zero-Sequence Impedance in Fuselage

Wire Size (AN)	A-C Resistance of 1 Conductor Per 1,000 Feet at 20 C	r_c A-C Resistance 3 Conductors Corrected to 30 C	A-C Resistance of 3 Wires and Ground Return at 30 C by Test	r_g at 30 C Ohms Per 1,000 Feet	h = 3.43″ Reactance Calculated by 0.353 $\log \frac{2h}{D_s}$	Reactance by Test h = 3.43″	x_g Ohms Per 1,000 Feet
20	10.25	3.550	3.720	0.170	0.750	0.772	0.022
*18	6.44	2.225			0.720		
16	4.76	1.650	1.48	−0.170	0.701	0.678	−0.023
**14	2.99	1.370			0.675		
12	1.88	0.651	0.681	0.030	0.650	0.671	0.021
10	1.10	0.381	0.432	0.051	0.618	0.577	−0.041
8	0.70	0.242	0.273	0.031	0.580	0.560	−0.020
6	0.44	0.152	0.210	0.058	0.548	0.547	−0.001
4	0.282	0.0977	0.204	0.1063	0.518	0.500	−0.018
2	0.188	0.0651	0.129	0.0639	0.488	0.492	0.004
Average				0.0425			−0.007
Corrected to 20 C				0.0408			

Values are for 1,000-foot units.

* No test values on number 18.

** Tests on number 14 were inconsistent. Measurements of wire indicated wire was smaller than number 14.

zero-sequence impedance for these conditions if wiring installations of this type are contemplated for future aircraft. It is probable that a new chart of corrective factors for r_g and x_g can be prepared to give values for these conditions, but much research remains to be done before such values can be verified and accepted as being reasonably accurate.

Appendix. Analytical Solution for Zero-Sequence Impedance in Aircraft A-C 400-Cycle Power Circuits

With certain data now available from the *B*-19 test, it is desirable to determine whether an analytical method of reasonable accuracy can be developed by which to extend these data to fit other conditions of installation. The basic problem, of course, is the inductive coupling between current flowing in a group of conductors and the same current returning through a flat sheet substantially parallel to the conductors, which depends largely upon the current distribution in the sheet. The physical relationship between the conductors and the sheet, with an approximation of the field distribution, is as shown in Figure 9.

The closest equivalent in electrical theory is shown by Figure 10, in which the return path through a uniform solid mass can be represented exactly by a line, *c′*, at a depth in the mass equal to the elevation (*h*) of the conductors above the surface of the mass, as long as the mass has infinite conductivity and extent. This line *c′* is known as the image and has neither resistance nor reactance. The impedance of such a circuit is computed as:

$$Z = r + j0.004657 f \log_{10} \frac{2h}{D_s} \text{ ohms per mile}$$

where

r = resistance of conductor or conductors in ohms

f = frequency, cycles per second

h = elevation of conductor above surface in inches

D_s = geometric mean radius of conductors in inches

When finite resistivity is introduced into the return path, along with limitation of the extent of the solid, certain modifications become necessary. Transmission-line calculations make use of Carson's investigations to determine a correction for earth-return paths, added to the formula as Z_g, whence

$$Z = r + j0.004657 f \log_{10} \frac{2h}{D_s} + Z_g \text{ ohms per mile}$$

Substituting a sheet of aluminum alloy for the earth return should not require material deviation from the method used for transmission lines. The sheet has appreciable resistance, however, which causes proportionately greater dispersion of the current path. The only method known which can be applied is to assume that the sheet is split longitudinally into a number of parallel strips; then to determine the current division among the strips as governed by self and mutual inductance and resistance. With the current division determined, it is possible to compute the effective "ground resistance" and the correction factor for its effect upon the reactance to the image, together known as Z_g.

Wagner and Evans show a method for determining the effect of an aerial ground wire in parallel with earth return[4] which they and their colleagues subsequently have expanded to include a counterpoise wire. This method can be used to analyze the effect of five parallel strips of the sheet return with reasonable accuracy, provided the strips are symmetrical about the center line. More than five strips involves considerably more complication and does not seem to give commensurate advantages

Table VII. Zero-Sequence-Impedance Data

$$Z_0 = 3\left(r_c + j0.353 \log_{10} \frac{2h}{D_s} + Z_g\right) \text{ Per 1,000 Feet}$$

r_c = Resistance of One Conductor Per 1,000 Feet ÷ 3

D_s = Group Geometric Mean Distance in Inches

h = Distance From Wire to Ground Return in Inches

$Z_g = r_g + x_g$ (Correction for Return Circuit—See Z_g Curves, Figure 5)

Three Wires Spaced One-Half Inch Apart

Wire Size AN	400-Cycle Resistance Ohms Per 1,000 Feet: 1 Conductor	r_c	Self Geometric Mean Distance: 1 Conductor d_s	Group D_s	Values of 0.353 $\log_{10} \frac{2h}{D_s}$ Ohms Per 1,000 Feet For h = 1″ to 5″: 1.0″	1.5″	2.0″	3.0″	4.0″	5.0″
20	10.25	3.42	0.0138	0.176	0.372	0.435	0.478	0.540	0.583	0.618
18	6.44	2.15	0.0174	0.190	0.361	0.422	0.467	0.528	0.573	0.608
16	4.76	1.59	0.0213	0.203	0.352	0.414	0.457	0.520	0.564	0.598
14	2.99	1.00	0.0268	0.219	0.339	0.400	0.445	0.506	0.552	0.584
12	1.88	0.627	0.0338	0.238	0.325	0.388	0.432	0.495	0.540	0.574
10	1.10	0.367	0.0452	0.261	0.311	0.374	0.418	0.481	0.525	0.560
8	0.70	0.233	0.0582	0.284	0.298	0.361	0.405	0.468	0.511	0.546
6	0.440	0.147	0.0734	0.307	0.287	0.349	0.394	0.456	0.500	0.534
4	0.282	0.094	0.0925	0.332	0.275	0.337	0.382	0.444	0.490	0.520
2	0.188	0.0627	0.1159	0.358	0.263	0.326	0.370	0.432	0.476	0.510
1	0.158	0.0527	0.1237	0.372	0.257	0.320	0.364	0.426	0.469	0.504
1/0	0.127	0.0423	0.145	0.353						

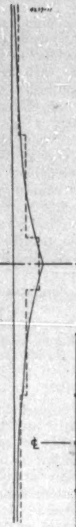


Figure 11 (left). Current distribution in return path through flat metal

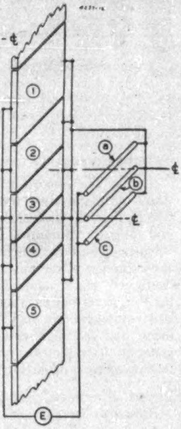


Figure 12 (below). Strip method for inductance calculations

It must be recognized that the actual distribution of current will be somewhat as shown by the solid curve in Figure 11. Computing the current as uniform in each of five sections of return path in effect substitutes a step curve for the smooth curve of Figure 11, indicated by the dash lines. Trial and error is the only method known for determining the proper widths of strips to compute, but excellent agreement with test data has been attained when the width of the middle strip has been taken as $2h$, the adjacent strips as $4h$, and the outside strips as $8h$.

This method requires that the sheet be considered to consist of strips as illustrated by Figure 12.

In the following calculations these symbols will be used:

R_x = resistance of conductor x
D_{xy} = geometric means distance x to y
R_{xx} = geometric mean radius of x, sometimes represented by D_x
d_{xy} = actual distance x to y, center to center

The identity of strip 3 is retained through all calculations as a reference for all distances.

The three conductors a, b, and c, are resolved into an equivalent conductor d by the following equations:

$$R_d=\frac{1}{\frac{1}{R_a}+\frac{1}{R_b}+\frac{1}{R_c}}=\frac{R_a}{3}$$

$$R_{dd}=\sqrt[9]{R_{aa}\,R_{bb}\,R_{cc}\,d_{ab}^2\,d_{bc}^2\,d_{ca}^2}$$

$$D_{3d}=\sqrt[3]{d_{3a}\,d_{3b}\,d_{3c}}=h+\frac{t}{2}\text{ approximately}$$

Strips 1 and 5 are combined into an equivalent conductor 6:

t = thickness of sheet
W_n = width of strip n

$$R_6=\frac{1}{\frac{1}{R_1}+\frac{1}{R_5}}=\frac{R_1}{2}$$

$$R_{11}=R_{55}=0.223\,(W_1+t)^*$$

$$R_{66}=\sqrt[4]{R_{11}\,R_{55}\,d_{15}^2}=\sqrt{R_{11}\,d_{15}}$$

$$D_{63}=\sqrt{d_{13}\,d_{35}}=d_{13}{}^{**}$$

$$D_{6d}=\sqrt[6]{d_{1a}\,d_{1b}\,d_{1c}\,d_{5a}\,d_{5b}\,d_{5c}}=\sqrt{D_{63}^2+D_{3d}^2}$$
approximately

Similarly, strips 2 and 4 are combined into another equivalent conductor 7:

$$R_7=\frac{1}{\frac{1}{R_2}+\frac{1}{R_4}}=\frac{R_2}{2}$$

$$R_{22}=R_{44}=0.223\,(W_2+t)^*$$

$$R_{77}=\sqrt[4]{R_{22}\,R_{44}\,d_{24}^2}=\sqrt{R_{22}\,d_{24}}$$

$$D_{73}=\sqrt{d_{23}\,d_{34}}=d_{23}{}^{**}$$

$$D_{7d}=\sqrt[6]{d_{2a}\,d_{2b}\,d_{2c}\,d_{4a}\,d_{4b}\,d_{4c}}=\sqrt{D_{73}^2+D_{3d}^2}$$
approximately

$$D_{67}=\sqrt[4]{d_{12}\,d_{14}\,d_{25}\,d_{45}}=\sqrt{d_{12}\,d_{14}}$$

Having established these conversions from physical to electrical relationships, it becomes necessary next to set up the equivalent circuit, Figure 13. By setting up simultaneous equations for the voltage drop in each leg of the circuit from M to N, then substituting the conditions $e_6=e_7=e_3$ and $E=e_d-e_3$, it is possible to derive the equations:

$$\frac{i_6}{i_d}=-\frac{Z_{6d}\,Z_{77}-Z_{7d}\,Z_{67}}{Z_{66}\,Z_{77}-Z_{67}^2}$$

$$\frac{i_7}{i_d}=-\frac{Z_{7d}\,Z_{66}-Z_{6d}\,Z_{67}}{Z_{66}\,Z_{77}-Z_{67}^2}$$

$$Z=Z_{dd}+\frac{i_6}{i_d}Z_{6d}+\frac{i_7}{i_d}Z_{7d}$$

in which

$$Z_{66}=R_3+R_6+j0.353\log\frac{D_{63}^2}{R_{66}\,R_{33}}\text{ ohms per 1,000 feet}$$

$$Z_{6d}=R_3+j0.353\log\frac{D_{3d}\,D_{63}}{D_{6d}\,R_{33}}\text{ ohms per 1,000 feet}$$

$$Z_{77}=R_3+R_7+j0.353\log\frac{D_{73}^2}{R_{77}\,R_{33}}\text{ ohms per 1,000 feet}$$

$$Z_{7d}=R_3+j0.353\log\frac{D_{3d}\,D_{73}}{D_{7d}\,R_{33}}\text{ ohms per 1,000 feet}$$

$$Z_{dd}=R_d+R_3+j0.353\log\frac{D_{3d}^2}{R_{dd}\,R_{33}}\text{ ohms per 1,000 feet}$$

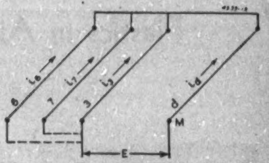
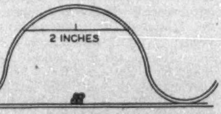


Figure 13. Current-path relationship derived from strip method

Table VIII. Impedance of Three Conductors in Parallel, Return Through Wings—Calculated From Test Data

Wire Size (AN)	D-C Resistance (Ohms Per 1,000 Feet) (by Test)	400 Cycle, by Test	
		A-C Resistance (Ohms Per 1,000 Feet)	Reactance (Ohms Per 1,000 Feet)
2	0.1005	0.2045	0.3025
4	0.1338	0.2320	0.417
6	0.1860	0.290	0.384
8	0.2665	0.380	0.390
10	0.3960	0.506	0.494
12	0.676		
†14			
16	1.430	1.558	0.567
18			
20	3.780	3.920	0.554

† Reading in Error.
Temperature: Approximately 30 degrees centigrade.



Scaled size and shape of duct in which wires were placed in wing

Table IX. Impedance of Three Conductors in Parallel, Return Through Neutral Wire Alone

Wire Size (AN)	D-C Resistance (Ohms Per 1,000 Feet) by Test	400-Cycle Impedance (Ohms Per 1,000 Feet) by Test	
		Resistance	Reactance
4	0.360	0.428	0.554
10	1.425	1.474	0.564
16	5.55	5.24	0.713

Table X. Impedance of Three Conductors in Parallel, Return Through Neutral Wire and Wing Structure

Wire Size (AN)	D-C Resistance (Ohms Per 1,000 Feet) by Test	400-Cycle Impedance (Ohms Per 1,000 Feet) by Test	
		Resistance	Reactance
4	0.129	0.213	0.346
10	0.396	0.525	0.426
16	1.427	1.570	0.544

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* Reference 5.

** Within the accuracy of this method of analysis.

Plastics in Aircraft Electricity

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SYNTHETIC organic plastics for electrical insulation represent one of the important contributions of the plastics industry to modern military and commercial aircraft. These materials for the most part are not used as substitutes for more suitable but scarce insulating materials, but have been selected because they possess superior mechanical and electrical properties.

It is the purpose of this paper to discuss some of the considerations involved in the selection and application of plastics and to survey under the headings, "Flexible Insulation" and "Rigid Insulation," the materials now being employed and their outstanding properties and limitations. Newer materials are described in connection with the applications for which their properties indicate special suitability, so far as the secrecy restrictions will permit.

Properties of Plastics

Although all plastics are good electrical insulators in the sense that their volume resistivities and dielectric strengths are all reasonably high, the various resins in all their modifications exhibit an enormous range of mechanical and electrical properties. Some of the "elastomeric" polymers resemble natural rubber in many respects but are definitely superior in such characteristics as resistance to sunlight and ozone and inferior chiefly in respect to elasticity. Some of the transparent plastics rival the transparency of crystal quartz, and others equal or exceed glass in resistance to certain types of abrasion. Among the synthetic resins are materials with volume and surface resistivities as high as those found in any natural substance. Some resins have power factors among the lowest found in solids. Though all organic resins are limited in their ability to withstand high temperatures, the heat resistance of certain ones is remarkably high. Chemical research is adding to the knowledge of molecular structure and as a result not only is producing new materials with remarkable properties but also is improving some of the older synthetic resins in striking ways.

Plastics suitable for electrical insulation have been discussed by many writers.[1,2,3] The chemical background for the electrical and other physical properties has been described.[4,5,6] Comprehensive handbooks are available in which plastics are described in detail.[7,8] A volume of authoritative data on commercial plastics has been issued and currently is being revised.[9]

Despite the large amount of published data, engineers and designers frequently have not been given adequate information to permit of an intelligent comparison of the various synthetic resins. Test methods and testing equipment only now are becoming standardized, and many of the older data are useless for comparison with data taken by other methods in other laboratories. For certain properties there are still no standard methods of measurement. The American Society for Testing Materials through its committee on plastics, *D*-20 and its committee on electrical materials, *D*-9 is laboring to improve this situation and now has on its books[10,11] a comprehensive array of testing methods and many more in preparation. Since many plastics show very different properties when tested under different sets of conditions of temperature and humidity, a temperature of 77 degrees Fahrenheit and a relative humidity of 50 per cent have been adopted as standard and now are being used quite generally throughout the plastics industry. The technical data book published by the Plastics Materials Manufacturers' Association[9] is a particularly useful source of data, because for each datum the test method used is reported.

To indicate some of the limitations of published data on plastics, the following three properties are discussed to which relatively little attention has been given in the literature.

FATIGUE EFFECTS IN PLASTICS

Among the important properties for which data are most limited are those which involve measurements of creep and fatigue. A number of papers have been published in which are described the behavior of certain materials under continued static and dynamic stressing,[12,13] nevertheless, standard methods and equipment are still under development, and the engineer is not yet in a position to predict the probable life of fabricated parts operating under stress.

IMPACT STRENGTH, SINGLE BLOW AND REPEATED IMPACT

For some applications the impact strength of rigid insulation may be a critical property. Even though the standard impact test is one of the simplest of measurements to perform, recent work has shown that variables in the preparation of test specimens are capable of causing wide variations in the impact strength of certain plastics. Thus, in one experiment in which the method of machining the standard notch was made the only variable, the impact strength of cellulose acetate ranged from 1.28 to 2.24 foot-pounds per inch of notch.

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$$Z_{67}=R_3+j\,0.353\log\frac{D_{63}\,D_{73}}{D_{67}\,R_{33}}\text{ ohms per 1,000 feet}$$

A typical calculation, using this method for three *AN*-2 conductors laced together and supported 3.43 inches from a sheet of 0.030-inch Dural, gives $Z=0.118+j0.482$ ohm per 1,000 feet, and $Z_0=3Z=0.354+j1.446$ ohms per 1,000 feet. Tests on the *B*-19 indicated $Z_0=0.387+j1.476$ ohm per 1,000 feet, which is excellent verification of the analytical method and demonstrates that it may be used safely to determine the impedance of other wire sizes and separations from the skin with ample accuracy.

The image theory gives $Z_0=0.195+j1.464$ ohm per 1,000 feet for the same installation, which is entirely satisfactory for reactance but inadequate in resistance, because of lack of provision for including the resistance of the skin. However, when corrected by $3Z_g=0.150-j0.021$, (Z_g from Figure 5 having a value of $0.05-j0.007$) value Z_0 increases to $Z_0=0.345+j1.443$ which is comparable with the preceding results.

Similar analysis of the four *AN*-10 conductors (one intended for isolated neutral but not used) installed in the space between the skin and the stiffening corrugations yields: $Z_0=1.416+j0.972$ ohm per 1,000 feet. Tests on the *B*-19 indicate $Z=1.518+j1.482$ ohms per 1,000 feet. This discrepancy appears to result from inability to include the adjacent corrugations in the five-strip method of analysis. Inasmuch as this type of installation is likely to be used but rarely, however, and only for limited portions of any aircraft, it seems probable that either of the methods of calculating the zero-sequence impedance will be found sufficiently accurate for determination of fault currents and co-ordination of protective equipment.

References

1. A-C ELECTRICAL SYSTEMS FOR LARGE AIRCRAFT (book). Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa. Chapter 6, page 5.

2. GENERAL ELECTRIC DATA FOLDER 63,004.

3. ELECTRIC-POWER TRANSMISSION (book), Woodruff.

4. SYMMETRICAL COMPONENTS (book), Wagner, Evans. Pages 164–70.

5. UNITED STATES BUREAU OF STANDARDS Number 169.

For this reason the data on impact strength appearing in tables of physical data should be used only for very general comparisons between materials.

In designing parts[14] which are to be subjected to impact, it should be noted that the impact strength of a carefully prepared test bar is almost certain to be different from the strengths of various parts of complex molded pieces. The standard test specimens are simple rectangular bars, the molding of which usually offers no difficulty. Large or irregularly shaped moldings, on the other hand, frequently present such difficulties in molding that the completed pieces may be very inhomogeneous and, therefore, exhibit very different physical properties from point to point. Table I shows the impact strength of bars cut from various parts of a large injection molding as shown in Figure 1. Note that the impact strength ranges from 0.65 to 3.55 foot-pounds per inch of notch. It is also of interest to observe that the value obtained on a regular injection-molded test bar is only 1.07 foot-pounds per inch of notch, much lower than that obtained on several of the specimens listed in the table.

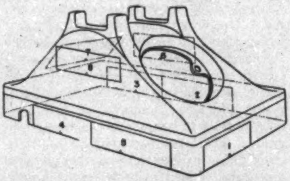

Figure 1. Injection molding showing locations from which the bars listed in Table I were cut

From discussion by I. R. Hopkins, *Proceedings*, American Society for Testing Materials, volume 43, 1943, page 1220. Used by permission

The above discussion applies to single-blow impact strengths. Since molded insulation may be subjected to repeated impacts, the fatigue values under this treatment are of importance. Several investigations have been made[15,16] in which test bars of different materials were subjected to multiple blows, each of which had less energy than was required to cause failure in a single blow. Though repeated-impact fatigue testing has been very limited in scope, the indications are that its results may serve to explain some failures of parts in service.

Figure 2 illustrates hand-operated apparatus of a simple type for studying repeated-impact fatigue, which follows a design of the Bell Telephone Laboratories, Inc. The test specimen is a standard Izod impact bar[17] which is clamped at the bottom of the steep trough. A graded set of steel balls is used, ranging in weight from 0.0022 to 0.55 pound. A ball is selected and is started down the trough from such a height that the energy is approximately equal to the single-blow impact strength. If the bar does not break, the ball is caught on the rebound, returned to the starting point, and allowed to fall again. If the initial energy is chosen correctly, two or three blows are sufficient to cause the bar to fracture. Enough bars are broken to determine the average number of blows of this energy needed to cause failure. A somewhat lighter steel ball then is substituted, and the procedure is continued until an energy value is found which the specimen will withstand for a large number of blows. The balls usually are started from the same height throughout an experiment, so that the energy of the blow is the variable, whereas the velocity is held constant.

In such hand-operated apparatus the maximum number of blows which can be obtained conveniently is not very large, but the shape of the initial portion of the curves shown in Figure 3 seems to be very significant. Thus the molded polymethyl methacrylate is able to withstand 0.145 foot-pound per inch of notch for 125 blows, while its initial or single-blow impact strength is 0.28 foot-pound. The impact strength at 125 blows is 52 per cent of the single-blow value. On the other hand, the cellulose acetate composition shown in Figure 4 has a fatigue strength of only nine per cent of its single-blow value. Repeated-impact machines which operate automatically have been developed[16,18] and data obtained on such machines indicate that the degradation of impact strength continues as the number of blows increases but that the rate of degradation continually decreases. Table II lists the fatigue impact strengths of several thermosetting and thermoplastic materials.

It should not be concluded from these data that plastics must not be used under conditions involving repeated impact. Many types now are being used with great success under the most severe service conditions. These studies do indicate, however, that in the selection of materials attention should be paid to fatigue under repeated concussions, and that life tests on completed parts of certain types probably should include repeated-impact testing in some form.

THE EFFECT OF ELEVATED TEMPERATURES

Knowledge of the effects of elevated temperatures on organic resins is extremely important in the design of parts, and many papers have been written on the physical properties of various plastics at high temperatures and on changes in properties resulting from prolonged heating.[19,20] Methods now are being established for exposure of plastics to standard heating cycles, after which each property in question will be remeasured. When these standard conditions become widely used, the data published on the resistance to continuous high temperatures will permit comparison among materials. At present no such comparison can be made.

The loss of rigidity at higher temperatures frequently limits the usefulness of parts made of plastics. This is particularly true of thermoplastics, but the thermosetting resins also suffer decrease in modulus of elasticity and exhibit higher creep under load at elevated temperatures. In many applications of plastics in insulation, however, the material is called upon to support extremely small loads, frequently only its own weight. There has been until recently no standard method suitable for obtaining data on the basis of which even a preliminary selection of a heat-resistant material could be made. The heat-distortion test, *D*648–41*T*, of the ASTM has been used for many years, and the "heat-distortion temperature" as obtained by this method frequently has been considered erroneously to be the upper limit of the temperature range in which the material is usable. That this is far from true has been demonstrated in practice, since many plastics are giving good service at temperatures well above their heat-distortion points.

Table I. Impact Strength of Bars Cut From Injection-Molded Piece Shown in Figure 1

Specimen	Foot-Pounds Per Inch of Notch		
	Minimum	Average	Maximum
1	2.20	2.66	2.89
2	2.58	2.99	3.21
3	2.48	2.62	2.73
4	1.98	2.49	2.87
5	2.70	3.08	3.55
6	0.67	0.83	0.98
7	0.69	0.72	0.77
8	0.65	0.78	1.00

Izod strength of standard injection-molded test bars 1.07 foot-pounds per inch of notch.

From a discussion by I. R. Hopkins, American Society for Testing Materials *Proceedings*, volume 43, 1943, page 1220. Used by permission.

In test method, *D*648–41*T*, a test bar is loaded at its mid-point while it is supported as a simple beam. The temperature of the surrounding air is raised at the rate of one-half degree centigrade per minute, and when the mid-point has been deformed ten mils, the "heat-distortion temperature" is considered to have been reached. This small deformation occurs as the result of several factors, including elastic deformation and plastic flow or creep. The load used is sufficient to cause a stress of 264 pounds per square inch in the outer fibers of the test specimen. Most thermoplastics yield to this stress at rather low temperatures, and at 80 degrees centigrade very few remain sufficiently rigid to resist the distorting

forces. Under service conditions, however, some thermoplastics can be used at temperatures many degrees higher than this, provided that they are not required to support a load. For other materials the heat-distortion temperature is evidently very near the maximum temperature at which they can be used, since the test bar loses its rigidity completely at temperatures only a few degrees higher than the heat-distortion point.

With the object of producing more significant data on the rigidity of plastics at elevated temperatures, test method *D648–41T* currently is being revised. By the simple expedient of finding the respective temperatures at which the deformation of 0.010 inch occurs when the specimen is subjected to two loads, large and small, the test distinguishes between those materials which lose their rigidity over a narrow temperature range and those which are able to sustain light loads at high temperatures, even though deformed readily by larger loads at much lower temperatures. The larger load produces a fiber stress of 264 pounds per square inch, whereas the smaller one is one fourth as large, so that the stress is 66 pounds per square inch.

Table III lists the heat-distortion temperatures of several thermosetting and thermoplastic resins as determined by the heat-distortion test in its revised form. The "low-load" heat-distortion temperature of molded nylon, 206 degrees centigrade, illustrates the unique position which this material occupies among thermoplastic resins. In certain applications in which it is not required to support a load, this thermoplastic can be used at temperatures usually considered endurable only by thermosetting resins.

In Figure 4 the deformation is plotted as a function of temperature for three materials. It can be seen that polymethyl methacrylate loses its rigidity completely over a temperature range of only a few degrees, whereas polyvinylidene chloride and nylon retain much of their rigidity at relatively high temperatures.

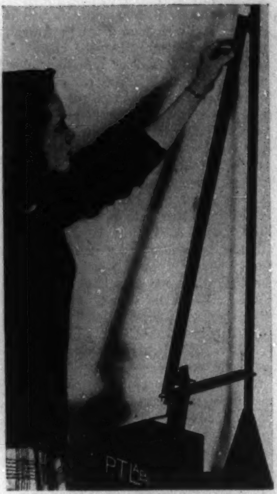

Figure 2. Hand-operated apparatus for measuring fatigue under repeated impact

Table II. Relationship Between Single-Blow and Repeated-Blow Impact Strength

Material	Relation of Repeated-Blow Impact Strength to Single-Blow Impact Strength, Per Cent	
	150 Blows*	1,000 Blows**
Laminated phenolics		
Paper base	50	33.3
Canvas base	45	24.4
Molded phenolics		
Wood-flour filled		59.4
Rag filled		37.7
Cast ethyl cellulose		2.43
Molded cellulose acetate H-2		7.65
Molded cellulose acetate H-4		9.05
Cellulose aceto-butyrate		7.13
Acrylate resin, cast	41	25.42
Nylon, injection-molded	58	

* From an unpublished laboratory report by R. Burns, Bell Telephone Laboratories, Inc.

** From paper by Lubin and Winans, reference 18.

As this revised test becomes established, the data should be very helpful in the selection of materials for use at elevated temperatures, but it should be remembered in using such data that they are the result of a short-time test and do not necessarily indicate the behavior under continuous loads.

Flexible Insulation—General Purpose

Rubber, the traditional wire- and cable-covering material, has been displaced largely by synthetic rubber and by "elastomeric" organic plastics. In Table IV some of the properties of synthetic wire-covering materials are compared. Highly plasticized polyvinyl chloride and the copolymers of vinyl chloride and vinyl acetate are used almost exclusively for general-purpose insulation of wire and cable in aircraft. Their principal limitations are: relatively low heat resistance and high power factors.

The use of such vinyl resins in wire covering had been increasing for several years before the rubber shortage stimulated the use of substitute materials. The Navy Department in 1936 began to use thermoplastic insulation for permanent shipboard cable because of its oil resistance and because it can be made nonflammable.

An enormous amount of development work by several laboratories[21,23] has been required to produce materials having the properties needed to meet the exacting requirements of aircraft insulation. In addition to maintaining a reasonably high insulation resistance over a wide range of temperature, such insulation must meet rigorous physical requirements, which include:

1). Sufficient strength to withstand rough handling and abuse at moderately high and at very low temperatures.

2). Enough flexibility to enable its being pulled through a considerable length of continuous conduit.

3). Nonflammability.

4). Moisture-resistance.

5). Flow properties which permit of rapid extrusion on wire and cable.

6). Freedom from embrittlement by age or by ozone.

The strength and rigidity of the plasticized polymers of vinyl chloride and vinyl acetate vary widely with temperature. Since these materials are thermoplastic, their rigidity decreases as the temperature rises, and 75 degrees centigrade is an upper limit for their use as wire covering. The lower limit of temperature at which they can be used is reached when the materials become brittle or not sufficiently extensible. By selection of plasticizers, stabilizers, lubricants, and pigments the lower temperature limit has been pushed to below −40 degrees centigrade. The large part which the plasticizer plays in determining the physical properties of such wire covering is indicated by the high percentage of plasticizer used—in the range of 35 to 45 per cent.

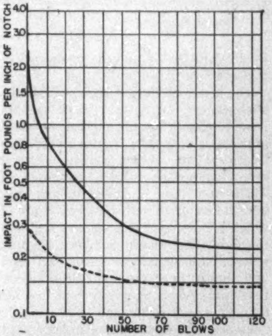

Figure 3. Energy of blow versus number of blows required to cause failure

Data were obtained on apparatus shown in Figure 2

--- Molded polymethyl methacrylate

— Molded cellulose acetate

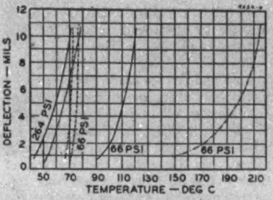

Figure 4. Deformation versus temperature at two fiber stresses using proposed revision of ASTM *D648–41T*

The temperature at which the deformation reaches 10 mils is defined as the heat-distortion temperature

--- Polymethyl methacrylate molding-power
—— Polyvinylidene chloride
-.-.- Nylon *FM*-1

Unfortunately, the improvement in the low-temperature flexibility is gained, at least in part, at the expense of strength at elevated temperatures. The modifiers also may cause more or less damage to the electrical properties. The permanence of the mechanical and electrical properties when the materials are exposed to continuous high temperatures requires that the plasticizers have low volatility and that suitable stabilizers be used. A plasticizer such as dibutyl phthalate, which imparts good flexibility at low temperatures, is somewhat volatile, and therefore insulation in which it is used gradually stiffens when exposed to continuous heat. Tricresyl phosphate, although less volatile, does not bring about flexibility at such low temperatures. Very extensive studies of plasticizers have been made, and the present extremely useful wire coverings represent a compromise between strength at elevated temperatures and brittleness at low temperatures.

These plasticized vinyl resins possess certain other characteristics which add greatly to their usefulness as insulation. No curing is required, because the resins are thermoplastic. Production is very rapid by means of extrusion techniques very similar to those used for rubber. The principal modifications of equipment have been made to provide better heat transfer and to eliminate pockets in which material could collect and then, on decomposing, cause damage to stock extruded later.[23] These vinyl resins are colored readily to facilitate the identification of conductors in complex wiring systems. The National Electrical Manufacturers Association recognizes eight standard colors, British government specifications designate 11 standard and three special colors, and manufacturers of wire-covering compounds have introduced still other colors.

Other thermoplastics which have been extruded as insulation or jacketing materials include polyvinylidene chloride, ethyl cellulose, polythene, and nylon. It is probable that each of these will find fields of usefulness as insulation. The heat resistance of nylon recommends it for uses as a jacket material in applications in which its toughness and other unusual physical properties are needed. Polyvinylidene chloride, which like nylon is self-extinguishing, is used widely as flexible tubing, and its use as electrical conduit has considerable possibilities. Being thermoplastic, it can be heated and bent to conform to the spaces into which it must fit, and the tubing has a smooth interior which would permit conductors to be pulled readily through long sections of conduit.

Flexible Insulation—
Special Purpose

IGNITION WIRING

Ignition cables and connectors are exposed to more severe conditions than is any other insulation used in aircraft. Heat, the enemy of all electrical insulation, is always present, with temperatures of 400 degrees Fahrenheit not uncommon, and while under such temperatures the insulation is subjected to vibration, mechanical stresses, high voltages, and corona. With the pressure changes which occur rapidly in flight, "breathing" of the insulation causes moisture to be forced into voids in the insulation. Under corona conditions this moisture becomes highly conductive. The insulation is exposed to oxidation by the ozone resulting from corona as well as by the nitric acid formed from the air and moisture under corona conditions.

Considering the severity of these conditions, it is not surprising that no completely satisfactory wire covering has been developed. The general practice has been to use a layer containing a high content of natural rubber next to the core, which is of small diameter to keep the capacitance of the cable to a minimum and which is made of stainless steel to obtain the necessary strength. An impregnated cotton braid was applied next followed by a jacket, frequently of ethyl cellulose. The cotton braid now is being replaced largely by glass braid, and jackets of neoprene now are commonly used. The entire wiring assembly is, of course, enclosed in metal shielding to eliminate radio interference.

Much experimental work has been done on cable design,[24] and considerable progress has been made toward developing longer-lived ignition insulation by utilizing synthetic plastics of higher heat resistance. Extruded nylon jackets, which have been applied to several types of cable, may prove to be suitable outer coatings for ignition cable.

HIGH-FREQUENCY CABLE

The use of high- and ultrahigh-frequency equipment in aircraft has led to demands for greatly improved coaxial cable. The insulation in such cables must have low dielectric losses to prevent excessive attenuation, must maintain the mechanical spacing of the conductors, and preferably should seal the cable against moisture. Polystyrene beads have been used widely to support the inner conductor, and by ingenious shaping of the beads it has been possible to produce a flexible cable. If jacketed with a moisture-resistant cover, the cables could be made satisfactory for use under conditions of high humidity.

In 1943 polyethylene, or polythene, first became commercially available in this country,[25] although it had been in use for several years in England. Polythene is a thermoplastic material with excellent electrical and good mechanical properties, which are maintained over a wide range of temperature. Table V lists some of the electrical and mechanical properties of polythene. The extremely low brittleness temperature (below −60

Table III. Heat-Distortion Temperatures at Two Loads Using Proposed Revision of American Society for Testing Materials D648–41T

Material	Proposed Method		
	Degrees Centigrade		
	T_1 264 Pounds Per Square Inch	T_2 66 Pounds Per Square Inch	$T_2 - T_1$
Polyvinyl chloride, rigid	62	65	3
Cast polymethyl methacrylate, ASTM type I	79	83	4
Polyvinyl butyral, rigid	56	60	4
Molded polymethyl methacrylate GP	72	76	4
Polystyrene	80	85	5
Cellulose acetate sheet	58	64	6
Cast polymethyl methacrylate ASTM type II	101	107	6
Cellulose acetate molded	58	68	10
Molded polymethyl methacrylate heat-resistant	90	98	8
Cellulose acetate butyrate	63	75	12
Ethyl cellulose	72	88	16
Melamine-formaldehyde	198	222	24
Urea-formaldehyde	141	172	31
Polyvinylidene chloride	71	121	50
Nylon, molded	77	206	129

degrees centigrade) is a property inherent in the resin itself, and therefore it does not change with aging as in the case of materials which owe their flexibility to impermanent plasticizers. The electrical properties of polythene are excellent, with high dielectric strength, low dielectric constant, and very low power factor, and both of the latter are constant over a very broad range of frequencies. The excellent moisture resistance, coupled with ease of extrusion on wire, permits the manufacture of moistureproof coaxial cable of low dielectric loss. Figure 5 shows the dielectric strength of polythene as a function of thickness

Polythene can be injection- or compression-molded as well as extruded, and can be applied as a moisture-resistant insulating coating by flame spraying or by depositing from solution. Its excellent mechanical and electrical properties should aid in the solution of many problems in electrical insulation.

Other flexible synthetic resins suitable for wire covering having low dielectric losses have been announced,[27] and others are under development. Knowledge of molecular structure has contributed greatly to the improvement of the electrical properties of plastics at high frequencies.[6] When the restrictions required by the national security can be lifted, this chemical development will be found to constitute one of the interesting developments of World War II.

Covering for Magnet Wire

Representative of plastics used in the form of solution for the coating of wire is polyvinyl formal. Extremely thin layers are depositied on conductors in successive layers to ensure complete coverage. This provides extremely tough and flexible insulation for wire used in winding the coils of motors, relays, transformers, and other magnetic devices.

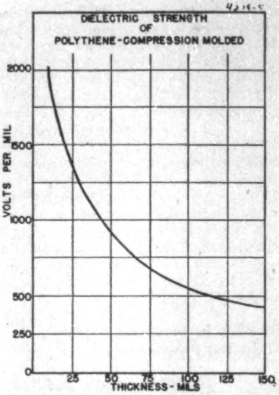

Figure 5. Dielectric strength of compression-molded polythene, short-time, using ASTM method *D149–40T*

The expanding use of small, heavy-duty electric motors in aircraft has, however, brought forward the need for wire covering of greater heat resistance than is offered by polyvinyl formal. New resins which have been found suitable for such high-temperature use are the organosilicon resins, the polymeric silicons, which, though lacking the toughness of other wire-coverings, are said to withstand continuously temperatures as high as 500 degrees Fahrenheit.

Rigid Insulation

General-Purpose Materials

By far the most widely used molded insulating material is phenol formaldehyde. A considerable variety of mechanical and electrical properties is made possible by the use of several formulations and a number of fillers.

Phenolic moldings are relatively low in cost and can be made in inexpensive molds in cheap compression-molding equipment. The introduction of high-frequency heating techniques[28] has increased the speed of production of large pieces, has permitted the making of thick-walled moldings, which was formerly impossible, and has improved the quality of many other moldings. Hundreds of phenolic molded pieces are used in aircraft for purely electrical reasons as well as in applications in which structural and electrical properties are of equal importance.

The principal limitations of the phenolics are set by their relatively high dielectric losses and by their tendency to track under an electric arc. The high water absorption of the high-impact materials has led to difficulties in the tropics. There the continued high humidity keeps the molded parts of electrical equipment saturated with moisture, and under these conditions bacteria and other organisms feed on the cellulosic fillers. Sometimes these growths cover the entire surface of the insulation with a conducting film which renders the equipment inoperative. This phenomenon is not peculiar to phenolic materials but might occur with any resin containing cellulose fillers.

Other materials which are used frequently for molding various parts of electrical equipment include cellulose acetate, cellulose acetate-butyrate, ethyl cellulose, polymethyl methacrylate, polystyrene, aniline-formaldehyde resins, urea-formaldehyde, melamine formaldehyde, and nylon. The molding techniques used for nylon have been described,[29] and this material has been used for a number of applications for which its

Table IV. Properties of Compounds Used for Insulation and Jackets

Chemical Type	Name	Use on Cable	Dielectric Constant	Resistivity and Power Factor	Dielectric Strength	Flammability	Cold-Resistance	Stress Strain Properties	Oil-Resistance	Resistance to Ozone and Sunlight
Isobutylene-diolefin, TS copolymer	Butyl rubber	Insulation	1	1	3	4	1	3	4	2
Ethylene polymer, T	Polythene	Insulation	1	1	1	4	1	2	3	1
Plasticized vinyl, T chloride polymer	Flamenol	Insulation	3	3	2	1	3	2	1	1
	Koroseal Vinylite	or jacket	*	*	*	1	2	2	1	1
Plasticized vinyl, T chloride vinyl acetate copolymer	Vinylite	Insulation	3	3	2	1	3	2	1	1
		or jacket	*	*	*	1	2	2	1	1
Butadiene-styrene, TS copolymer	Buna S	Insulation	2	2	2	4	4	3	3	3
		or jacket	*	*	*	4	4	2	4	4
Butadiene-acronitrile, TS copolymer	Buna S	Jacket	*	*	*	4	3	1	2	4
Chloroprene polymer, TS	Neoprene GN	Jacket	*	*	*	1	3	1	2	1
Organic polysulfides, TS	Thiokol	Jacket	*	*	*	4	3	1	1	1
Polyamide, T	Nylon	Jacket	*	*	*	1	3	1	1	1

Code: T—Thermoplastic. TS—Thermosetting.

The materials have been compared on the basis of physical data reported for commercially available and commonly used formulations, the smaller numbers indicating relatively higher quality.

* These properties not significant in jacket stocks.

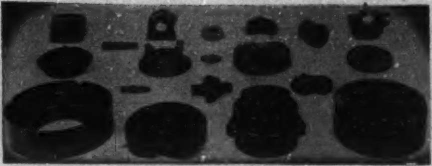

Photo courtesy American Cyanamid

Figure 6. Melamine formaldehyde molded ignition parts

strength in thin sections and its heat resistance make it particularly suitable.

A new thermoplastic molding composition has recently been announced[30] under the trade name of Cerex. This material is reported to have an ASTM heat-distortion temperature of 212 degrees to 230 degrees Fahrenheit, a dielectric constant of 2.7 and a power factor of 0.0024, and can be injection molded.

IGNITION PARTS

Magneto parts, distributor heads, and other molded parts of the aircraft ignition system have presented many difficult problems. Although molded mineral-filled phenol formaldehyde moldings have proved to be highly satisfactory in automotive engines and are now serving in tank and marine engines, this material has a serious weakness which has limited its usefulness in high-voltage systems in airplanes. Under the heat of a spark or arc, the surface of the plastic decomposes, the carbon-to-carbon bonds are broken, and a carbon "track" forms which is conductive, so that the high-voltage spark jumps across the insulator. At the high altitudes now being reached by fighting planes such flashovers quite frequently take place because of the low breakdown strength of the air at low pressures. If this flashover causes a track to form, the part is put out of service permanently, since the spark will continue to follow the carbon track even after return to lower levels.

For these reasons a specially compounded rubber has usually been used in such ignition parts. Much effort has been put forth to find a more suitable material, and asbestos-filled melamine formaldehyde has been employed in several ignition parts.

This resin has a chemical structure made up of carbon-to-nitrogen bonds, rather than carbon-to-carbon bonds as in the phenolics. Though it will fail by tracking under the heat from a high-energy spark or arc, the time required for this track to appear is much greater than for the phenol formaldehyde. Thus, under the arc-resistance test of the ASTM most phenol-formaldehyde resins fail in less than 60 seconds, whereas asbestos-filled melamine formaldehyde survives for periods up to 190 seconds. At high altitudes repeated flashovers can occur across a melamine-formaldehyde molding without damage, and, when lower altitudes are reached and the insulating strength of the air is restored, the undamaged part will function as before.

Melamine-formaldehyde resins[31] have a high modulus of elasticity and a small elongation at failure. These factors, coupled with the large coefficient of thermal expansion which is common to organic resins, makes it difficult to mold around metallic inserts without the occurrence of cracks on cooling, or later in service, due to the stresses around the inserts. The difficulties of molding have been complicated further by the large size of the metallic inserts as designed for use with hard rubber, which at elevated temperatures needs the re-enforcement provided by the metal. The melamine, on the other hand, not only does not need this re-enforcement but also is weakened by the large inserts, which leave less resin to stand the stresses caused by the differential expansion and contraction. By reducing the sizes and changing the shapes of the inserts it has been possible to make satisfactory moldings, some of which are shown in Figure 6.

This molding problem is characteristic of the situation encountered when one material is to be substituted for another. Frequently changes in design are needed to provide full advantage of the properties of each new material.

MOLDED PARTS FOR USE AT HIGH FREQUENCIES

The dielectric losses of phenolic resins have been greatly reduced both by use of large amounts of mica filler and by chemical changes in the resins. The resulting materials have found wide use in communication equipment.

Polystyrene in its various modifications is the principal synthetic resin used for rigid insulation in applications involving high and ultrahigh frequencies. Molding techniques have been developed to overcome many of the difficulties encountered in molding polystyrene, so that injection molding of complex parts is now common practice. The dielectric losses of polystyrene are extremely low, the dielectric constant being 2.5 and the power factor less than 0.0003.

The principal limitations of polystyrene are its relative brittleness and its loss of rigidity at slightly elevated temperatures. Several modifications of polystyrene have been made in which the heat resistance has been greatly improved and the excellent electrical properties maintained. This has been achieved by one manufacturer[32] through combining polystyrene with a chlorinated diphenyl. The resulting material, although lacking the transparency of poly-

Table V. Typical Properties of Polythene

Property	Test Results*	Test Method
Specific gravity	0.92	*D*71–27
Tensile strength, pounds per square inch		**D*638–42*T***
At −70 degrees Fahrenheit	5,000	
At +77 degrees Fahrenheit	1,700	
At +170 degrees Fahrenheit	700	
Elongation, at 77 degrees Fahrenheit, per cent	30–500	**D*638–42*T***
Stiffness, 77 degrees Fahrenheit, pounds per square inch	13,300	**D*747–43*T*
Impact, Izod, foot-pound per inch of notch		**D*256–41*T*
At −70 degrees Fahrenheit	0.5	
At +77 degrees Fahrenheit	4.0	
Heat-distortion temperature, degrees Fahrenheit		Modified
Fiber stress = 66 pounds per square inch	122	*D*648–41*T*
Deformation under load, 122 degree Fahrenheit, per cent	20	†*D*621–43
Specific heat, calorie per gram per degree centigrade	0.5	
Coefficient of expansion, per degree Fahrenheit	10.5×10^{-5}	*D*696–42*T*
Dielectric strength, 1/8 inch, volts per mil	450	*D*149–40*T*
Volume resistivity, ohm centimeter	10^{17}	*D*257–38
Dielectric constant, maximum	2.3	*D*150–42*T*
Power factor, maximum	0.0006	*D*150–42*T*
Brittleness temperature	Below −60C	*D*746–43*T*
Water absorption, per cent	0.01	*D*570–42
Flammability	Ignites and burns slowly	
Resistant to	Water, alkalies, acids, and oxygenated solvents	
Not resistant to	Chlorinated solvents, aliphatic and aromatic hydrocarbons at elevated temperatures	

* Average values reported. May vary considerably with method of preparation of specimen (injection, compression, and so forth).

** Tensile tests at no-load crosshead speed of 1.0 inch per minute.

† Load used was 1,200 pounds per square inch since the 4,000 pounds per square inch load of standard test was designed for more rigid plastics.

styrene, has a dielectric constant of 2.55 and a power factor of 0.0004, when measured at radio frequencies. Its heat-distortion temperature is 85 degrees centigrade, whereas that of unmodified polystyrene is 76 degrees to 78 degrees centigrade. Because the strains introduced by molding are less, it is claimed that the temperature at which moldings of the modified material begin to lose their shape is much higher than the heat-distortion temperature would indicate. The material has the additional advantage of being nonflammable.

Another chemical modification of polystyrene is reported[1,30] to have a heat-distortion temperature of 110 to 120 degrees centigrade, equal to that of unfilled phenol-formaldehyde molding material. That this high heat resistance is gained without injury to the electrical properties is indicated by the values reported for dielectric constant and power factor—2.6 and 0.0002, respectively.

Plastics as Replacements for Mica

The tremendous increase in electronic equipment which requires high-quality capacitors as blocking condensers and as parts of the frequency-controlling circuits has created a great demand for first-quality mica. Difficulties attending the procurement, splitting, transporting, and handling of block mica have stimulated the search for substitutes. Although no completely satisfactory all-purpose substitute has been developed, the ceramics of high dielectric constant are serving in many of the smaller capacitors, and several plastics have been used in condensers of certain other types. One of these, a thermoplastic sold under the trade name Polectron, is reported to have a dielectric constant of 3.0 and a power factor of 0.0004 to 0.001 at frequencies of 1 kilocycle to 100 megacycles. The heat-distortion temperature is reported to range from 100 degrees centigrade to 150 degrees centigrade, depending on the plasticizer content. The resin can be compression- and injection-molded, and a technique has been developed to impregnate electrical equipment without the use of solvents. The mechanical properties are reported to be generally good, although the impact and flexural strengths are relatively low.

Polystyrene tape has been developed of sufficient uniformity in thicknesses of about 0.001 inch for use as the dielectric of capacitors of the rolled type. The dielectric constant of polystyrene (2.5) is a limitation, but its high dielectric strength and low power factor are very desirable properties in a capacitor dielectric. Such capacitors cannot be used at elevated temperatures, and it has been reported that in life tests under high direct voltages the polystyrene dielectric has not proved to be entirely satisfactory.

The excellent electrical properties of polythene, shown in Table V, together with the ease with which it can be extruded as tape, indicate that polythene also may prove useful as a capacitor dielectric.

Conclusions

It has been shown that synthetic resins have gone a long way toward solving the multifold electrical problems which are encountered in airplane design. This solution has not been found through the development of a single material containing all desirable properties, but rather by judiciously employing various materials, singly or in combination, each of which has certain outstanding properties. Thus we see polystyrene used because of its low dielectric losses, polythene because its excellent electrical properties are combined with flexibility at low temperatures, polyvinyl chloride and polyvinyl chloride acetate because of their nonflammability, flexibility, and abrasion resistance, nylon because of its toughness and because its strength properties are retained at elevated temperatures, melamine formaldehyde because of its resistance to tracking, phenol formaldehyde because of its good properties and cheapness. The research laboratories of the country are continuing to work on new synthetic resins which will make possible improved electrical equipment and on improved testing methods which will produce data of greater value to designers.

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Alternating Versus Direct Current for Aircraft-Radio Power Supply

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Synopsis: Recent designs of aircraft-radio equipment have provided for operation from existing aircraft electric systems, and little attention has been given to providing the electric system that is best suited for radio equipment use. This has been a natural and proper approach, because of the relatively small load requirements of aircraft radio. Accordingly, comparatively little attention has been given to the effects of the type of power supply on the characteristics of aircraft radio equipment. It is the authors' belief that a review of the subject is desirable at this time to assist aircraft designers in making a proper choice of the electric system to be provided on future aircraft designs, as well as possible modifications to the electric systems on aircraft now in use. This paper considers the effects of the various types of aircraft electric systems on the characteristics of the radio equipment, from the standpoints of weight, efficiency, reliability, simplicity, flexibility, and emergency operation. The various types of a-c and d-c power supplies are discussed, and consideration is given to the effects of frequency and number of phases on a-c power supplies.

ONE of the first applications of radio to aircraft took place during World War I when aircraft were used for observation purposes. The early transmitters used at that time were spark transmitters. The early aircraft generators were 115-volt 900-cycle single-phase a-c generators with an output of 200 watts. The generators were of the inductor type and were self-excited by a d-c winding on the rotor. A spark transmitter was incorporated in the streamlined end cover of the machine, while the tuning unit was located in the fuselage of the airplane.

After the war, with the advent of transmitters using vacuum tubes, it was necessary to provide low-voltage filament current, in addition to the high-voltage plate current. Two types of generators were developed: one a dual-voltage d-c generator, the other an inductor alternator, with a d-c winding on the rotor of sufficient capacity to furnish the filament supply and battery-charging current. Dynamotors came into use soon after storage batteries were incorporated on airplanes and are still used extensivley to provide high-voltage plate current for most of the radio equipment on airplanes equipped with d-c systems.

Some of the early dual-voltage d-c generators were supplied with an overrunning clutch, so that the generator could be run as a dynamotor for emergency communication, taking power from the battery of the airplane. As the demand for low-voltage direct current on airplanes increased, many aircraft electrical designers preferred to generate all the power as low-voltage direct current, using dynamotors to supply the plate voltage for the radio equipment. Until about 1920 most of the d-c systems used on airplanes were six-volt systems, but around 1925 12-volt systems were inaugurated and later came into general use.

The United States Navy had used several types of radio equipment powered by 800-cycle 115-volt single-phase wind-driven and engine-driven alternators. When electric-power demands on aircraft became greater, the Navy was responsible for the development of engine-driven generators with separate a-c and d-c generators combined in one frame. These generators have been developed over a period of several years until combination generators are now available as listed in Table I. Since most of the applications using these generators are for main engine-driven generators, the speed ranges of the generators are such that the frequency of the a-c supply varies over a two-to-one range. Aircraft generators of this type have been used by the Navy for several years and have given very satisfactory performance. Radio equipment powered by these generators uses step-up transformers and rectifiers for plate-current supply, and step-down transformers are used to supply the low-voltage filament current. The d-c output of the generator is used for operating other electrical devices on the airplane, and to operate relays, lights, and band-switching motors in the radio equipment.

Motor loads and other applications for d-c power on aircraft have grown very rapidly in the past few years, and have led to the use of 24-volt d-c systems in most modern aircraft, with dynamotors supplying the high-voltage plate current for the radio equipment.

Larger airplanes of the future probably will require available electric generating capacities of more than 100 kw. The weight of the wiring for transmitting this much power with a 24-volt d-c system is excessive, and a study of the optimum voltage for aircraft systems indicates that higher voltages were advisable.[1] The difficulties of breaking and controlling direct current at high altitude discourage the use of higher-voltage d-c systems for the larger aircraft. It is also difficult to obtain good commutation on small lightweight aircraft motors operating on voltages appreciably higher than 30 volts.

A 115-volt three-phase 400-cycle system was designed for use on the Douglas *XB*-19 airplane. Auxiliary engines were used to drive the alternators, with sufficiently accurate governors to allow paralleling. A rectifier was incorporated in the system for battery charging and supplying other d-c loads that could not utilize alternating current. Tests on the *XB*-19 installation were very encouraging and led to plans to incorporate three-phase a-c systems on new design multi-engine airplanes. An operating voltage of 208 volts has been proposed in order to attain minimum wiring weight. The proposed a-c system would use a grounded neutral so that single-phase 115-volt 400-cycle alternating current may be taken from any of the three-phase leads and ground. The metal airplane structure would be used for the neutral, making a four-wire system unnecessary. Thus, in addition to the three-phase 208-volt 400-cycle alternating current, there will be a supply of single-phase 115-volt 400-cycle alternating current available on the new-design airplanes; also a nominal amount of direct current supplied by either rectifiers or motor generator sets will be available.[2]

It will be seen from this brief history that, as the development of aircraft electric systems progressed, the influence of radio apparatus on the choice of the system had less significance than with the earlier systems. It is true, however, that the choice of the aircraft electric system does affect to a considerable degree the size, weight, and other characteristics of aircraft radio equipment. Therefore, it is the authors' belief that an analysis of the effects of the aircraft electric systems on radio apparatus is desirable to enable aircraft and radio apparatus designers to evaluate its proper significance in determining the choice of the best over-all aircraft electric system.

The remainder of this paper consists of an analysis of these characteristics of aircraft radio equipment, together with certain conclusions resulting therefrom.

The authors have taken a commonly used aircraft radio transmitter having an output of 100 watts and a standard receiver as being representative of the radio equipment used on modern aircraft, and have studied the weights and performance of this equipment with various

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types of power supplies. While the exact figures may not be correct for other ratings and designs, it is believed that an analysis of these two pieces of equipment will show any major trends that will be prevalent in most aircraft radio apparatus.

It is hoped that the conclusions reached in this paper will be of assistance to the aircraft radio designers in designing radio equipment for new airplanes, as well as to the aircraft designers who must provide power for radio on their airplanes.

The aircraft electric systems listed will be studied only to the extent in which they affect the aircraft radio equipment. It is believed that so much work has been done on motors, generators, and the like, that a discussion of these factors would not add materially to the value of this paper.

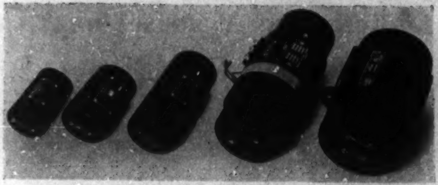
The voltages listed are nominal system values and for the low-voltage d-c systems are the rated voltage of the storage batteries that are incorporated in the system. Equipment for use on these systems often is rated at a different voltage than the nominal value. For instance, a 24-volt system may use dynamotors rated at 27.5 volts, and the generators may be rated at 28.5 or 30 volts.

List of Radio Power Supplies Available and Proposed

*6 volts—direct current.
*12 volts—direct current.
*24 volts—direct current.
*115 volts—direct current.
24 volts—400 to 800 cycles, single-phase alternating current.
115 volts—60 cycles, alternating current.
*115 volts—400 cycles, single-phase constant frequency.
115 volts—400 to 800 cycles, single-phase alternating current.
115 volts—400 cycles, three-phase alternating current.
*115 volts—800 to 1,600 cycles, single-phase alternating current.
115 volts—800 cycles, constant-frequency alternating current.
208 volts—400 cycles, three-phase alternating current.

Radio-Equipment Power Supplies

Regardless of the electric system of the airplane, plate circuits of radio equipment operate from direct current. The reason is that the generation of radio-frequency energy, in the case of transmitting equipment, and the amplification and demodulation of radio-frequency signals, in the case of receiving equipment, require the use of relatively high direct voltages. The direct voltages may be in the order of 200 or 300 volts for receivers, and as high as 5,000 volts for transmitters, while 20,000 to 30,000 volts may be required for some special electronic equipment. Therefore, it is always necessary to provide a conversion means whereby the relatively low voltage of the airplane's system, either alternating or direct current, is converted to high-voltage direct current suitable for operating the electronic tube circuits of radio equipment. The type of power supply available on a given airplane largely determines the type of conversion equipment used.



Frame sizes range from $2^{3}/_{4}$ inches to 5 inches in diameter, and output ratings range from 15 to 250 watts

Figure 1. Various sizes of dynamotors available for providing high-voltage direct current for radio power supply

In addition to the afore-mentioned power requirements, practically all the electronic tubes in radio equipment require electrically heated cathodes, and this energy also must come from the electric system of the airplane, either directly or through conversion equipment. Usually, the cathode heaters are designed for low-voltage operation, whereas a wide variety of voltages are required for different tube types. By using indirectly heated cathodes or the proper circuit arrangements, the tube-heater energy usually can be supplied by either a-c or d-c sources. Heater energy may be supplied directly from the d-c system of the airplane when a 24-volt system is used, since many tube types are designed with 24-volt heaters; or several lower-voltage tubes may be connected in series across the 24-volt line.

When the airplane is supplied with an a-c system, the heater energy may be supplied by low-voltage windings on the high-voltage transformer, or separate filament transformers may be used.

Conversion Equipment

The following types of power supplies, or conversion equipment, for obtaining high-voltage direct current for operation of aircraft radio, are in use on modern aircraft or have been considered for use on aircraft to be built in the future.

Dynamotors are generally used to supply the high-voltage direct current for radio equipment on airplanes provided with low-voltage d-c systems. Many ratings and sizes of dynamotors are available, and several types are shown in Figure 1. Dynamotors are also available that incorporate additional windings on the armature, thus having two or even three output voltages. An armature for a dynamotor having three output voltages is shown in Figure 2.

Rectifier power supplies consist of step-up transformers that raise the low-voltage alternating current from the airplane's system to high-voltage alternating current, which then is changed to direct current by means of rectifying devices, usually consisting of electronic tubes.

Vibrator conversion equipment may be used to convert low-voltage direct current to alternating current by interrupting the steady direct current at some arbitrary frequency by a mechanical interrupter or vibrator. The pulsating direct current is then transformed to the proper high-voltage a-c value, and rectified to produce high-voltage direct current. Rectification of the high-voltage alternating current usually is accomplished by means of a rectifier tube or tubes, but, in some applications rectification is affected mechanically by means of contacts vibrating synchronously with the interrupter contacts, thus eliminating the rectifier tubes. Vibrator conversion systems are used to some extent on aircraft for applications requiring relatively small amounts of power but at present are not used as extensively as dynamotors. Figure 3 shows a dynamotor and a vibrator power supply, both having an output of 60 milliamperes at 250 volts direct current.

Rotary inverters (in the aircraft industry, any power device that changes direct current to alternating current, either a motor generator set, rotary converter, electronic equipment, or a vibrator, is commonly called an inverter) sometimes are used to convert the direct current of the airplane's system to alternating current of an arbitrary frequency. The resulting alternating current then is converted into high-voltage direct current by using transformers and rectifier tubes. Inverters are used in aircraft where dynamotors cannot be used because of high direct voltages required, or because alternating current is necessary for the operation of the equipment.

Electronic inverters have been built in the laboratory by which direct current has been converted to alternating current using grid-controlled rectifier tubes. No moving parts are used in this type of converter. At the present time, however, it is doubtful if electronic inverters are

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* Systems in present-day use on aircraft.

Figure 2. Armature for a dynamotor having three separate output voltages

developed to the point where they are suitable for radio-equipment power supply.

Electronic inverters sometimes are used to provide a source of direct current where extremely low-current drain is imposed. A new vacuum tube has been developed that lends itself very well to this application, because it operates with both the filament and the plate supplied from 24 volts direct current. This tube has an output of 0.7 watt when used in this manner. The tube is connected as an oscillator and tuned to a medium frequency, and a radio-frequency transformer is used to raise the output of the oscillator to a high voltage, which is then rectified. This output may be used as bias for the elements of cathode-ray tubes, or for similar applications which require high voltage but have low current drain.

Weights and Efficiency as Affected by Power Systems

The weights and efficiency of the various types of power-supply components for an aircraft transmitter with a rated radio-frequency output of 100 watts are shown in Table II.

From this table, it may be seen that the weights of the power-supply components are less for an 800-cycle single-phase power supply than for any other system listed. It will be noticed that for a-c power supplies the weight increases greatly as the frequency decreases. The dynamotor weights included in Table II are for separate dynamotors for the master oscillator and the power amplifier, and some weight savings could be gained by using a single dual-voltage dynamotor.

Table III shows weights and efficiency of aircraft-receiver power supplies for a standard aircraft receiver having a sensitivity of better than 15 microvolts for six milliwatts audio output. The weights and over-all efficiencies have been tabulated for various airplane power supplies as listed. Weights and efficiencies also have been calculated for a 60-cycle single-phase power supply for both Table II and Table III in order to provide a comparison with the values obtained at commercial power frequencies.

Tables II and III are not fully conclusive as to the relative total weights of radio equipment using different power-supply systems, since the weights of the generating equipment are not listed. The pounds per kilowatt for the various ratings of aircraft generators vary over a wide range. For example, a 15-volt 100-ampere d-c aircraft generator, having a minimum speed of 2,500 rpm, weighs approximately 23 pounds per kilowatt of output, while a 30-kw three-phase 208-volt 400-cycle generator, operating at 6,000 rpm, weighs approximately 2.8 pounds per kilowatt of output.[3]

For combination a-c and d-c generators now in production, the 800-cycle single-phase output weighs about 10 to 12 pounds per kilowatt. Therefore, in order to determine which power supply will result in the lowest over-all weight, generator and wiring weights also must be considered, and a tabulation of calculated over-all weights is shown in Table IV.

In the case of variable-frequency power supplies, the weight of radio power supply is determined by the lowest frequency. For example, the weight of the radio power supply will be essentially the same when designed for use with a constant-frequency 800-cycle system as when designed for a variable-frequency 800- to 1,600-cycle system.

Reliability and Servicing Simplicity

In any equipment installed on aircraft reliability is of prime importance. Much of the navigation of the airplane is dependent upon radio communication between the airplane and the ground, and in many cases the lives of the passengers and crew may depend on the reliability of the radio equipment. In considering reliability, all the various types of power supplies have certain weaknesses. It, therefore, is necessary to examine all the types under consideration to choose the one that will be the most reliable.

Ease of servicing is also a very important feature of a satisfactory power supply for aircraft radio, especially under conditions frequently encountered in modern warfare. Since aircraft radio equipment must be designed so that it can be serviced with a minimum of time and trouble, the equipment should use a minimum of small parts and incorporate arrangements whereby defective or worn parts may be replaced without dismantling the equipment.

One definite advantage of a d-c system using dynamotors for high-voltage plate-current supply is that the power to operate the radio equipment is taken from the main power-supply bus of the airplane. As a result, power to operate the radio equipment is available as long as either generators or batteries deliver sufficient current to the bus. Most of the military multiengined airplanes have a small, auxiliary engine-driven power plant that may also be paralleled with the other generators on the airplane. D-c systems, therefore, lend themselves very well to providing emergency communication, since the radio equipment may be operated for a short time from the battery of the airplane alone. D-c systems using dynamotors also have the advantage that the radio equipment does not need rectifier tubes or transformers, which leads to a simplification of the radio circuits.

Moving parts are always subject to wear, and every dynamotor has a rotating armature, two or more commutators, and at least two sets of brushes. Except for the smaller ratings, a starting relay generally is used to control the dynamotor. The usual maintenance for a d-c rotating machine is necessary at regular intervals if satisfactory reliability is to be maintained. Maintenance of dynamotors is a problem in itself, since spare brushes, bearings, armatures, and other parts are required. This problem has been alleviated somewhat by mounting the dynamotors in such a manner that they are secured to the radio equipment by snap fasteners, while the electric connections are made by quick disconnect plugs and receptacles.

Some of the newer radio equipment for use on airplanes equipped with a d-c system requires higher voltages than may be obtained conveniently by the use of dynamotors. Usually this equipment has been designed to operate from a rotary inverter which takes d-c power from the airplane system and converts this power to alternating current which

Table I. A-C–D-C Power-Supply Engine-Driven Generators

Engine-Driven Generators and Carbon-Pile Voltage Regulators

Single-Phase A-C–D-C Generators

Military Designation (Navy Type)	Volts	Watts	Cycles at Rpm	Volts	Amperes	Speed Range (Rpm)	Weight in Pounds
NEA-3	115	1,200	800 at 2,400	28.5	60	2,400 to 4,200	45.0
NEA-4	115	1,200	800 at 4,000	28.5	100	4,400 to 8,000	32.0
NEA-5	115	1,200	800 at 4,000	28.5	200	4,400 to 8,000	45.0
NEA-6	115	3,500	800 at 4,000	28.5	75	4,400 to 8,000	45.0
NEA-7	115	2,500	800 at 4,000	28.5	150	4,400 to 8,000	45.0

Blast-cooling requirements: Blast equal to six inches water static plus velocity head.

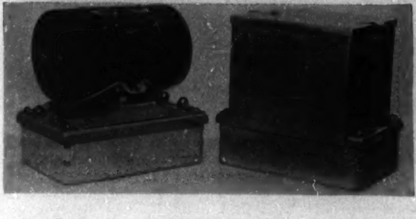

Figure 3. Dynamotor and vibrator power supplies with filter equipment in mounting base

Both have the same rating, that is, 0.060 ampere, 250 volts

is then stepped up to the required high voltage by transformers and rectified. The reliability of this system is quite high, from the standpoint of the radio equipment alone, but the inverter adds a hazard similar to the hazards common to a system using dynamotors, except that the hazards common to an a-c power supply are also present: namely, rectifier tube and transformer failures. Maintenance also is increased over either an a-c power supply or a d-c power supply using dynamotors, since both the inverter and the a-c transformer–rectifier unit requires servicing and maintenance.

Electronic inverters should use gas- or vapor-filled rectifier tubes such as the mercury type in order to have good regulation and efficiency. The extreme variations in ambient temperatures encountered in aircraft service may result in improper operation or damage to these tubes. Tubes using a mercury-pool cathode cannot be used for aircraft service, because, when the airplane is being maneuvered, the mercury would not remain in its proper position.

Some advantage may be gained in servicing simplicity by using vibrator power supplies instead of dynamotors. Vibrators for receiver power supplies have been developed that are hermetically sealed, thus making the vibrator capable of being used at high altitudes. The vibrators are small units, slightly larger than most receiving-type vacuum tubes, and the connections are made by means of a bayonet-type base that fits into a socket on the power supply. Since the vibrator is the only component of a vibrator power supply that is subject to wear, servicing is relatively simple, consisting mainly of replacing the vibrator unit.

The reliability of vibrator power supplies has been proved from experience gained in extensive use in automobile receivers. They are cheaper to build than dynamotors and require slightly less critical material. The high-voltage output of a vibrator-type power supply is not limited by commutation or armature insulation, as it is in a dynamotor. By using a transformer with more than one high-voltage winding, several output voltages may be obtained. The interrupter contacts and associated moving parts used in vibrator power supplies are a possible source of trouble which may render the unit unsatisfactory or inoperative.

Power supplies operating from alternating current have been in use on commercial radio equipment for many years, and their reliability has been unsurpassed by any other type of power supply. A-c power supplies consist of an assembly of transformers, rectifier tubes, filter reactors, and capacitors. Figure 4 shows alternating current to high-voltage d-c conversion components as used in the 800-cycle power supply of the transmitter studied for the weight tabulations. Since transformers, reactors, and capacitors can be made very reliable, the most vulnerable components are the rectifier tubes. These rectifier tubes usually are of the high-vacuum type and are suitable for operation over the entire range of ambient temperatures encountered in aircraft. Furthermore, the high-vacuum rectifier tubes do not require filament preheating as do mercury vapor or other similar rectifier tubes. High-vacuum tubes are very rugged and ordinarily do not cause trouble in aircraft service.

From the standpoint of servicing, it is believed that the a-c system is the most simple system to service. There are no moving parts to wear out or demand maintenance, since the process of rectification is accomplished by means of vacuum-tube rectifiers. Practically the only servicing requirement is the occasional replacement of rectifier tubes.

Radio Interference

Radio interference is a serious fault of any aircraft electric equipment. Interference may be segregated into two types: that carried by the wiring of the airplane, and that radiated from the source of interference, or from the wiring leading to the interference source. Radiated interference often is picked up by the antenna of the receiving set. It is important that interference be suppressed as closely as possible to its source, since suppressers located at a distance from the source may filter out the interference carried by the leads, but may permit interference to be radiated from the wiring between the suppresser and interfering element, thus causing serious interference in the receivers. For this reason, most aircraft dynamotors have mica capacitors mounted inside the dynamotor housing, connected from the brush holders to the ground, to suppress the radio interference set up by commutation and brush sparking. It is also important that transmitter dynamotors as well as receiver dynamotors incorporate interference suppressers. Capacitors and filter chokes for filtering the output of dynamotors are sometimes mounted in a box forming the mounting base of the machine.

Audio-frequency radio interference is commonly called "ripple," and is especially harmful when the d-c plate-current supply of a receiver contains an audio-frequency a-c component. Ripple may also be objectionable in the filament supply unless tubes with indirectly heated cathodes and properly designed filter circuits are used. This type of interference may be produced by variations in the output voltage of dynamotors caused by slot pulsations in the air-gap flux, by commutation transients, or by any other phenomena that results in an audible a-c component impressed on the output voltage of the dynamotor. Skew-

Table II. 100-Watt Aircraft-Transmitter Power-Supply Weight and Efficiency Comparison

Component	A-C Systems				D-C Systems	
	115-Volt 60-Cycle 1-Phase	115-Volt 400-Cycle 1-Phase	208-Volt 400-Cycle 3-Phase	115-Volt 800-Cycle 1-Phase	24-Volt D-C Dynamotor	24-Volt D-C to 400-Cycle Inverter* 1-Phase
Transformers	61.0	19.0	19.0	8.6		19.0
Rectifier tubes	0.7	0.7	1.7	0.7		0.7
Filter capacitors	20.0	4.7	2.2	2.2	1.75	4.7
Filter reactors	20.2	1.8	1.8	0.8		1.8
Compensating capacitor				3.4		6.8
Switches	1.5	1.5	1.5	1.5	1.5	1.5
Fuse	0.1	0.1	0.1	0.1	0.2	0.2
Housing	8.0	5.0	5.0	4.0	2.0	2.0
Inverter						21.0
Dynamotor					33.0	
Regulator				3.6		
Starting relay					1.75	1.5
Total weight	111.5	32.8	31.3	24.9	40.2	59.2
Input: Watts	875	875	350	875	1127	1460
Conversion efficiency (per cent)	88.5	88.5	91	88.5	69	53

* 24 volts direct current to 400-cycle, single-phase rotary inverter operating 400-cycle radio equipment. Radio-tube filaments heated directly from 24-volt d-c system.

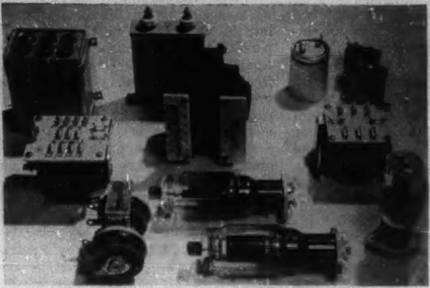

Figure 4. Components for 800-cycle single-phase power supply used on a 100-watt transmitter

ing the armature slots of a dynamotor and staggering the armature punching grain direction generally will minimize slot ripple to a nonobjectionable value.

A-c power supplies produce very little radio-frequency interference. Although the aircraft a-c systems operate at frequencies in the audio spectrum, it is relatively easy to filter out ripple from the rectifier output by means of properly designed filter circuits.

Rotary inverters use d-c motors as a driving means and tend to produce interference from brush sparking and commutation which may be carried over into the a-c output. For this reason power supplies using an inverter should be filtered carefully, and precautions should be taken to prevent interference radiation from the low-voltage leads. On some inverters harmonics are present in the output frequency which may necessitate additional filtering.

Vibrator power supplies present peculiar problems in filtering, because of the sparking of the vibrator contacts, while the harmonics present in the output wave form require special attention to filter design, shielding, and circuit arrangement. However, a well-designed vibrator power supply may be made to compare favorably with a dynamotor in the matter of radio-frequency interference.

Electronic inverters, built to deliver approximately 1,000 watts of high-voltage direct current have been found to present considerable filtering difficulties. Radio-frequency disturbances are set up by the arcs in the mercury-vapor tubes, and the output wave form has a high harmonic content. Special filtering means are required. However, for very small amounts of power, where radio frequencies are used for the inverters with high-vacuum tubes, radio-frequency interference may be eliminated by proper shielding and filtering.

Flexibility

Voltages required by electronic equipment on aircraft may range from 1.5 volts to 20 or 30 kilovolts, and usually a great number of different voltages are required on one airplane. This extreme range of voltages required can be obtained very readily if the aircraft has an a-c system, since by using transformers and rectifiers any required direct voltage may be obtained. For d-c systems, when more than one value of high voltage is required, it is necessary to use either dynamotors with additional windings and commutators or a separate dynamotor for each voltage. Of course, voltage dividers may be used to obtain various voltages for receivers or for grid bias and screen-grid voltages in transmitters. However, many transmitters require at least two high voltages for plate-current supply which would be inefficiently obtained by using voltage dividers. Because of commutation and insulation difficulties, the maximum output voltage of dynamotors has been limited to approximately 2,000 volts; and, where higher voltages are required on airplanes equipped with a d-c system, it has been common practice to use inverters to convert the direct current to alternating current, raise the alternating voltage to the desired value by a transformer, and rectify the output of the transformer to attain the desired high-voltage d-c power.

The efficiency of an aircraft inverter is seldom over 60 per cent, and the efficiency of an a-c power supply is about 90 per cent. Since the efficiency of the d-c generator, that supplies the power to drive the inverter, is approximately 75 per cent, a system using inverters has an over-all efficiency of about 40 per cent from the engine to the input of the radio equipment. Because of the extremely low efficiency of an inverter power supply, it would be better to generate alternating current by means of engine-driven generators.

Three-Phase Power Supplies

The three-phase 208-volt 400-cycle systems planned for use on the new airplanes are to be constant-frequency systems. The alternators will be driven from the main engines, with constant-speed drives interposed between the engines and the generators to enable parallel operation of all generators.

The amount of power required by receivers is so small that there is little to gain by operating them from three-phase current, but a slight saving in weight may be gained by using three-phase power supplies for high-powered transmitters. However, the use of three-phase current for transmitter power supplies necessitates more complicated transformers and the use of at least one more rectifier tube than single-phase power supplies. When considered as to servicing simplicity and ease of maintenance, the three-phase power supply is at least $1^{1}/_{2}$ times as complex as is the single-phase system. Therefore, a careful analysis should be made before using three-phase power supplies for aircraft radio equipment. In some cases, a special generator is used to supply current for radio equipment requiring approximately eight kilovolt-amperes. When the generators must be engine-driven and mounted on the engine accessory pad of one of the main engines of the airplane, a lighter generator may be built by using a three-phase design. In many cases this may mean that the generator will be light enough to withstand successfully the severe stresses induced on the generator by engine vibrations, where a single-phase generator might fail because of its greater weight. In

Table III. Aircraft-Receiver Power-Supply Weight and Efficiency Comparison

	Weight in Pounds				
	A-C Supply			D-C Supply	
Component	115-Volt 60-Cycle 1-Phase	115-Volt 400-Cycle 1-Phase	115-Volt 800-Cycle 1-Phase	24-Volt D-C Dynamotor	24-Volt D-C Vibrator
Transformers	13.0	3.5	1.9		
Filter reactor	3.0	0.6			
Filter capacitors	1.4	0.3	0.5	1.35	
Rectifier tube	0.2	0.2	0.2		
Housing and miscellaneous	2.5	1.6	1.2	1.0	1.0
Dynamotors				4.0	
Vibrator					4.0
Total weight	20.1	6.2	3.8	6.35	5.0
Input: Watts	75	75	75	66	63
Conversion efficiency (per cent)	68	68	68	78	81

this instance a three-phase power supply would be very advantageous from an over-all point of view.

Three-phase rectifier systems have slightly higher efficiencies than single-phase rectifiers, but usually the amounts of power involved are small enough that a slight change in efficiency will not have a notable result on the over-all power-supply requirements. A three-phase system will have the advantages of taking power from the main electric bus of the airplane and having a minimum of moving parts. Three-phase rectifiers will have a varying ripple component caused by supply-line-voltage unbalance between the phases. In Figure 5 the relationship between ripple of the rectified output and voltage unbalance of the three-phase line is shown for two filter combinations. Voltage unbalance is expressed as negative-phase sequence in per cent of the positive-phase sequence. The normal ripple frequency of a three-phase full-wave rectifier is six times the line frequency. Filter *A* has an attenuation at the ripple frequency of 23.1 decibels and has a resonant frequency of 1.5 times the line frequency. Filter *B* has an attenuation at the ripple frequency of 25.8 decibels, and its resonant frequency is 1.33 times the line frequency.

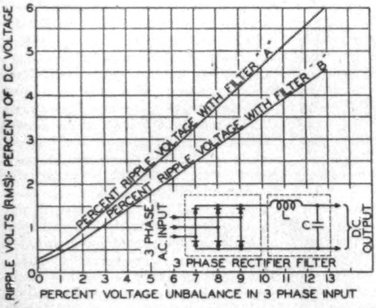

Figure 5. Effect of supply-line voltage unbalance on ripple in output of three-phase full-wave rectifier using two filter combinations

The additional filtering necessary to to eliminate this ripple will increase the weight of the filter system. The degree of line-voltage unbalance to be expected will be dependent upon the capacity of the system and the degree of load unbalance. It is believed that an unbalance in voltage between phases of five per cent will not be unusual.

Variable-Frequency Systems

The most common variable-frequency radio power supplies in use today are those on Naval aircraft. Combination a-c and d-c generators are used, and are usually driven from the main engines of the airplane. The a-c output of these generators is 120 volts, single phase, 800 to 1,600 cycles. The a-c generators are not paralleled, but usually at least two of the combination generators are installed on multiengine aircraft in order that the radio equipment may be operated, if one of the generators fails, by switching to another generator. On the larger Naval aircraft an auxiliary engine-driven generator is provided so that communication may be carried on while the aircraft is not in motion. The d-c output of the generators supplies power to the other electric equipment on the airplane and is also used to operate relays, motors, and the like, in the radio equipment. Generally about one third of the available generating capacity is alternating current.

Reliability of the afore-mentioned system is very good. Since the a-c generator is usually of the inductor type, with both the field and armature windings on the stationary member, while the rotating member is but a laminated-toothed rotor, there is little maintenance required for the generator. There are no brushes, slip rings, or moving windings. The various direct high voltages required are obtained by means of transformers and rectifiers, but low-voltage direct current is available and may be used to operate the filaments of vacuum tubes, relays, and motors. Stand-by communication is not available unless the airplane is equipped with an auxiliary power plant.

Other variable-frequency systems have been developed and used to a limited extent. In one system variable-frequency alternating current is generated and used to power radio and other loads suitable for use with alternating current, while the motors and other equipment requiring direct current are operated by rectifying a portion of the generated alternating current. Various voltages and frequencies have been used experimentally, the most common of which is 400 to 800 cycles, 24 or 115 volts. By the proper choice of transformers and rectifier units, the d-c supply may be any voltage desired. In the case of multiengined airplanes, each generator has its own rectifier–transformer unit so that it is not necessary to parallel the generators, but the d-c output from the rectifiers is paralleled to a common bus. The alternating current may be taken directly from one or more of the generators, with provisions made to connect the loads in parallel, in the event of failure of one or more of the generators.

Reliability of this system is almost the same as that of other a-c systems, except for the additional hazards introduced by the rectifiers. However, rectifiers of the dry-plate type have been developed that will successfully handle the power involved. Very little is added to the maintenance problems of the system by the inclusion of the rectifiers, since usually no moving parts are required. However, in some instances, forced cooling of the transformers and rectifiers is accomplished by a small d-c motor driving a fan, which requires the normal maintenance generally required by a d-c aircraft motor.

Radio Accessories

Relays used in radio equipment are affected by the type of power supply as to weight and size. D-c-operated relays will have smaller and lighter magnetic circuits and coils than will a-c-operated relays. On the other hand, smaller contacts and less contact travel may be used on relays handling alternating current, since the current passes through zero every half-cycle. In general, relays on aircraft radio equipment should be actuated by direct current if it is available on the airplane.

Band-switching motors are required to deliver high torque for a short time. D-c motors are better suited for this purpose than a-c motors, but high-torque a-c motors have been developed that will perform this function satisfactorily. Usually a-c band-switching motors will be heavier than d-c motors, especially if single-phase motors are used, since it is necessary to use a split-phase motor with a series capacitor in one winding in order to get high torque. This, consequently, results in additional weight.

The small panel-indicator lights on radio equipment should use low-voltage lamps in order that the filaments will not be made of wire too fine to withstand the vibration to which they will be subjected. On low-voltage d-c systems, the lights may be operated directly from the system voltage. For high-voltage d-c systems, series-dropping resistors are necessary; and, when used with a-c systems, either series-dropping resistors or a step-down transformer may be used to operate the panel lights. It is accepted practice to use the voltage available from the output of one of the filament transformers for this purpose.

When carbon-button microphones are used for voice communication, a small amount of low-voltage d-c power is required. Very low ripple content in the microphone current is imperative since any ripple will be amplified and heard as an objectionable hum in the receiver.

Direct current for the microphone circuit can be obtained directly from the airplane's low-voltage d-c system if the proper attention is given to filtering. For high-voltage d-c supplies, a series-dropping resistor or potentiometer may be used to obtain the required microphone current.

For a-c systems a small selenium or copper-oxide rectifier may be used, but good filtering of the output is important. When the airplane has low-voltage direct current available, it is advisable to obtain the microphone current from this source.

Radio-Equipment Performance

From the standpoint of radio transmitters, there is little difference in the performance characteristics of the equipment when operating from a d-c system than when operating from an a-c system. With proper filter design, the keying transients in the direct output voltage can be made to be about the same for rectifier-type power supplies as for dynamotor supplies. The frequency stability of transmitters is a function of the voltages applied to the plates and screen grids of the master oscillator tubes; except in the case of crystal-controlled oscillators, which tend to maintain constant frequency, regardless of plate voltage. Therefore, the extent to which the supply-line voltage remains constant determines to a great degree the frequency stability of the equipment, since most aircraft transmitters do not use crystal-controlled oscillators.

In aircraft receivers, operation from alternating current presents an additional problem which is of greater consequence when using frequencies higher than 60 cycles. The alternating current in the tube heaters, and wiring tends to carry over into the audio circuit and is amplified, appearing as an objectionable hum in the receiver output. The audio circuits of the receivers are particularly sensitive to the high frequencies proposed for aircraft electric-supply systems. However, this problem may be minimized by careful receiver design.

Summary

In summary, it may be said that:

1. The weight of radio equipment and conversion units with a-c power supplies is less than that of corresponding units with a d-c power supply.

2. The over-all power input to the radio equipment is less with a-c power supplies than with d-c power supplies.

3. From the standpoints of simplicity and reliability, a 400-cycle single-phase system appears to be the best. An 800-cycle supply is not greatly different, except that it is assumed that a special generator unit would have to be provided for radio power supply, whereas the 400-cycle power would be taken from the main supply bus on airplanes equipped with three-phase 400-cycle electric systems.

4. An a-c supply, using high-vacuum rectifier tubes, produces a minimum of radio interference, as compared with other systems. The choice of frequency does not greatly affect this characteristic.

5. A-c systems provide greater flexibility by permitting operation over a wide range of input supply voltages as well as readily providing for a multiplicity and wide range of direct and alternating output voltages.

6. Radio apparatus, operating from a d-c system, can provide communication in an emergency by drawing power from the storage batteries which are carried in the airplane and charged by the main generators. This means of emergency operation is equally available with either dynamotor, vibrator, or inverter conversion units. In the case of radio equipment operated from alternating current, emergency power can be obtained only from an auxiliary generator unit. This is usually provided for in larger aircraft by means of a separate engine-driven generator set.

Table IV. 100-Watt Aircraft-Transmitter Power-Supply and Electric-System Weight Comparison*

Component	Weight in Pounds 115-Volt 400-Cycle 1-Phase	208-Volt 400-Cycle 3-Phase	115-Volt 800-Cycle 1-Phase	24-Volt D-C Dynamotor	24-Volt D-C† 115-Volt 400-Cycle Inverter
Transmitter power supply	32.8	31.3	24.9	40.2	54.2
Generators	2.5	2.4	10.5	7.1	8.3
Wiring	3.0	2.0	3.0	4.0	4.5
Total weight	38.3	35.7	38.4	51.3	67.0

* This tabulation includes the weight of the transmitter power-supply components as listed in Table II, as well as that portion of the aircraft generators and wiring weight which is chargeable to supplying the radio load.

† 24-volt direct current to 400-cycle single-phase rotary inverter operating 400-cycle radio equipment. Radio-tube filaments heated directly from 24-volt d-c system.

7. Accessories common to radio equipment, such as lights, relays, motors, and microphone circuits, can more readily utilize a low-voltage d-c supply than an a-c supply.

Conclusions

A consideration of the factors of reliability, efficiency, servicing simplicity, flexibility, and minimum radio interference leads to the conclusion that for aircraft-radio power supplies a-c systems are to be recommended. The choice of frequency does not greatly affect this recommendation, but single-phase-operated radio equipment is to be preferred to three-phase equipment, even though the aircraft electric system may provide three-phase power for other uses.

The matter of determining the optimum type of radio power supply is complex, because a number of factors in addition to the weight of the actual radio power-supply components must be considered. These additional factors include frequency, total power capacity of the electric system, voltage, and minimum generator speed, and are usually determined by considerations other than the requirements of the radio apparatus, except on the smallest airplanes.

Taking all of the afore-mentioned factors into account, the following conclusions have been reached by the authors, as applying to four general types of commercial and military airplanes:

1. *Large Multiengine Aircraft.* Where 400-cycle three-phase systems supply the major portion of the airplane's electric load, it is recommended that the radio equipment be operated from one phase of the supply system. If a low-voltage d-c system is also available on the airplane, it is recommended that it be utilized to operate relays, band-switching motors, tube filaments, microphone circuits, and the like.

2. *Large Aircraft Using D-C Systems.* On large aircraft which are now using direct current for the entire electric system, and which are using rotary inverters to power a-c-operated radio equipment, considerable savings in weight may be gained by employing combination a-c and d-c generators, or by providing separate a-c generators. An 800-cycle supply for the radio equipment will result in minimum weight.

3. *Aircraft Using Dynamotors.* For aircraft now equipped with dynamotor power supplies for the radio equipment, weight savings may be attained by using a combination a-c and d-c system, with the high-voltage direct current supplied by transformers and rectifiers.

4. *Single-Engine Aircraft.* For single-engine aircraft, where radio-equipment power requirements are a major portion of the load, a combination a-c and d-c generator would again save appreciable weight.

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Design Considerations for D-C Aircraft Generators

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Synopsis: Engine-mounted aircraft generators must fit within an envelope $6^{1}/_{2}$ inches in diameter by 14 inches long. Electric-power requirements on military airplanes are increasing so rapidly that the most urgent assignment confronting designers of aircraft generators is that of achieving the ultimate output possible from this envelope. In 1939 the maximum rating was 1,500 watts; in 1940 the maximum rating jumped to 3,000 watts; in 1942 to 6,000 watts. A rating of 9,000 is coming into use, and a rating of 12,000 watts is likely to be possible in 1944. A rating of 15,000 watts has been mentioned. New materials, new military demands, new design ingenuity, new manufacturing techniques, and new operating experiences have all had a part in introducing more advances in this field during the past four years than were made during the approximate 20 years of generator history prior to 1938. Methods by which these advances have been accomplished are described, and possibilities for future progress are indicated.

HEART of all present-day electric systems for American combat airplanes is the 28.5- or 30-volt main-engine-driven d-c generator. This paper will be confined to such generators, and particularly to considerations determining the maximum outputs which can be obtained from them. Aircraft generators of this type are classified in Army Air Forces nomenclature as "accessories, engine-mounted." This designation is somewhat misleading; there is nothing "accessory" about the indispensable role being played by these generators on modern combat airplanes. In fact, the day when large airplanes will not be able to fly at all without electric power is not far distant, if indeed, it is not already at hand. Even airplanes such as the *B*-17 Flying Fortress and the *B*-24 Liberator, which can be flown after a fashion without electric power, cannot fight effectively without it. Each of these airplanes carries a normal complement of four generators. Failure of two generators will handicap combat performance; failure of more than two generators will prevent operation of the power-driven turrets, and the airplane will have scant chance of returning from its mission.

Modern combat airplanes use from one to six of these engine-mounted generators. Additional generators, driven by auxiliary engines, are provided on large airplanes to supply electric power while the main engines are idling or are at standstill. The only reason for mentioning these auxiliary-power units in this paper lies in the fact that a 15-horsepower 6,000-rpm engine is now available with which standard generators designed for main-engine mounting are used, thus multiplying the number of conditions under which these generators must be capable of operation. Aircraft electric-power demands are increasing almost daily, and, in spite of the enormous amount of work in progress on a-c systems for aircraft which are under development, existing aircraft will continue to use d-c systems, and the principal concern of the designer of generators for these airplanes is to wrest the maximum possible amount of power from the limited space and weight permitted for the generator. The growth of aircraft electric systems, both in connected load and in generator capacity, is shown in Figure 1. Already 300-ampere generators are in production, and 400-ampere generators are under development. The principal types of 30-volt d-c generators are listed in Table I.

Requirements for Engine-Mounted Aircraft Generators

The principal requirements for aircraft generators may be summarized as follows:

Dimensions: Assigned envelope, $6^{1}/_{2}$ inches in diameter by 14 inches long (not including splined shaft extension). Actual maximum diameter for many engines, 6 to $6^{1}/_{8}$ inches.

Weight and Overhung Moment: Weight varies with rating; the maximum weight an engine will support is from 45 to 50 pounds. Overhung moment varies with rating; the maximum value is usually 270 inch-pounds, although some recent engines will support 300 inch-pounds.

Mounting: All engine-driven generators are flange-mounted and are provided with a splined drive shaft to suit the internal spline provided by the engine manufacturer. All large generators have a five-inch bolt circle to suit six three-eighths-inch studs provided on the engine pad. The diameter of the pilot fit on the engine is $4^{1}/_{8}$ inches.

Vibration: Generators must withstand the vibration excitations encountered on aircraft engines. The flange mounting should stand linear accelerations of 60*g* applied at the center of gravity of the generator. The coupling should withstand alternating accelerations of 12,000 radians per second per second (of the accessory drive).

Ratings: Ratings in use vary from 1.5 kw to 9 kw. Ratings of 12 kw and 15 kw are desired. A summary of principal ratings is given in Table I. Two basic speed ranges are required; *low-speed* generators operate from 2,500 rpm or less to 4,500 rpm, and *high-speed* generators operate from 4,000 rpm or slightly higher to 10,000 rpm.

Altitude and Ambient Temperature Range: Figure 2 shows the current Army-Navy specification limits for ambient-temperature range at various altitudes. Operation is required over the entire temperature range and over a specified range of altitude. For combat airplanes, a maximum altitude of 50,000 feet is usually specified.

Ventilation: "Blast-tube" cooling is provided for all ratings above 50 amperes. A total head of from six to ten inches of water is provided according to the particular specification being followed. Two-inch tubes are used for ratings up to, and including 300 amperes; three-inch tubes for larger ratings. Methods of specifying blast-tube ventilation have been examined by C. G. Veinott in a recent paper.[1]

Excitation: The limited current rating of existing voltage regulators fixes the maximum permissible generator excitation at eight to nine amperes. Specifications require full output of the generator when it is operating at minimum speed with a resistance of 0.75 ohm added to the generator-field circuit to simulate regulator and wiring resistances. Compounding, if used, must not cause instability at high speeds, and must not cause polarity reversal in the event the reverse-current relay is closed with the generator at standstill.

Temperature Rise: Generators may be operated at any temperatures at which the designer can obtain the required output and the required endurance.

Endurance: For military service, generators may be serviced once at 500 hours, and then replaced completely at 1,000 hours. Brushes should last 100 hours at 30,000 feet, or a total of 500 hours of normal operation without replacement.

Mechanical Considerations

Mechanical and electrical design problems are so thoroughly interrelated in engine-mounted generators that it is impossible to discuss electrical design without considering mechanical limitations at almost every point. It is impossible to place too much emphasis on the impor-

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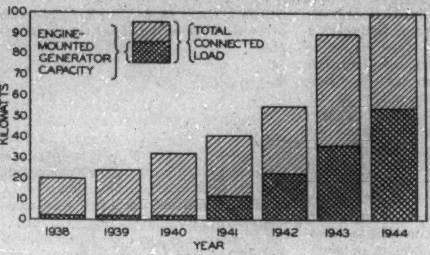

Figure 1. Engine-driven-generator capacity and total connected load for military airplanes

Values are maximum for any airplane in the designated year. The chart does not include experimental airplanes and does not include auxiliary power plants

tance of mechanical endurance, and with present trends toward higher operating speeds, mechanical problems are becoming more and more difficult to solve.

Mechanical problems start with size and weight. Engine specifications have long stipulated that an envelope $6^{1}/_{2}$ inches in diameter be reserved for the generator, but in the days when generators were rightfully termed accessories, other accessories were allowed to encroach upon the space allotted to the generator, with the result that generators for the older engines are limited to a maximum diameter of 6, or at most, $6^{1}/_{8}$ inches. Generators require protuberances such as air spouts and terminal connections, and they are made so that they may be mounted in 12 or sometimes even in 24 positions so that the protuberances can be dovetailed into the niches available. The new, large engines now coming into use provide more space for generators, and specifications for 300- and 400-ampere generators permit a diameter of $6^{3}/_{8}$ inches and require only six mounting positions. Even so, in an envelope about the size of a one-third horsepower 1,750-rpm industrial motor, the aircraft-generator designer must provide 5.7 kw at 2,500 rpm and 9 to 12 kw at 4,400 rpm.

Minimum weight is a stringent requirement for all aircraft components, and, in the case of aircraft generators, a definite limit for maximum weight and for overhung moment is set by the fatigue and shock resistances of the engine pad on which the generator is mounted. Naturally, the limiting weight applies only to the maximum rating for each minimum speed; if technical advances stretch maximum ratings upward, similar methods applied to earlier generators will reduce the weight of those machines.

If a given standard of design is assumed, output is a function of amount of active material and of minimum operating speed. Regardless of the ingenuity of the designer, and regardless of advances in materials and techniques, a point is eventually reached where a major increase in output can be obtained only by increasing the minimum speed of the generator. Figure 3 shows the relation between rating and kilowatts per pound for various 2,500- and 4,000-rpm generators constructed with materials in current use. Minimum speed and basic speed range are, of course, fixed by the particular specification being followed, and the generator designer has no choice as to operating speed unless he is willing to incorporate gears within his machine. He may concentrate, however, on the design of high-speed generators with every assurance that high-speed drives will appear in greater and greater abundance; power demands are already here which require outputs obtainable only from high-speed maximum-capacity generators; new engines are being equipped with high-speed drives as a matter of course; and there is a definite probability that some of the older engines will eventually be provided with high-speed drives.

Reference to Figure 3 shows that the gain in kilowatts per pound is less than proportional to the increase in speed as comparisons are made between low-speed and high-speed generators. An obvious reason is found in the fact that, while the output for each pound of active weight is very nearly proportional to speed, commutator size and total brush area do not remain constant, but instead also increase in proportion to rating. Another reason is that maximum speeds are so high that more material is required to hold the rotating parts together on high-speed generators; maximum stresses at overspeeds are nearly five times as high as on low-speed generators.

Ventilation is more difficult on high-speed generators, since air ducts are more restricted and greater losses are concentrated within the machines than is the case with low-speed machines of similar size. High temperatures multiply the difficulties of securing armature bands and wedges. Bands, for example, must be mechanically fastened independently of the solder ordinarily used to secure them. The use of solder for balancing is, to say the least, extremely hazardous. Wedges must be made of high-temperature high-strength material such as phenolic- or melamine-resin impregnated glass. Some machines use partially closed slots, the armatures being wound by inserting partially formed coils into the slots from the end opposite the commutator, and then forming the commutator ends of the coils. Armature leads are usually brazed into the commutator risers; this construction is essential for heavy-duty high-speed generators, as commutator temperatures frequently exceed 200 degrees centigrade.

Commutators are undoubtedly the most critical items of all the components which concern the designer of high-speed generators. They must stand temperatures from -55 to $+250$ degrees centigrade and speeds up to 12,000 rpm without distortion of more than 0.0001 inch between adjacent bars, or more than 0.0005 inch total indicated eccentricity. Brush life on high-speed generators depends very largely on the degree of perfection achieved in commutator design and manufacture. A good commutator used on a well designed machine will permit operation of brushes at densities well over 200 amperes per square inch; a commutator which becomes distorted at high speeds will wear brushes rapidly at 100 amperes per square inch.

All in all, the apparently easy path to increased ratings through increased operating speed is full of pitfalls. Even on low-speed generators, mechanical failures far outnumber electrical failures, and such electrical failures as occur are usually the result of previous mechanical failures. This is intended as no reflection on mechanical designers; rather it is intended to emphasize the magnitude of mechanical problems, and to point out that these problems multiply with increased size, with higher operating speeds, and with higher operating temperatures. High speeds are both necessary and desirable, but in general, as speeds increase, the problems of the generator designer multiply by some unformulated exponential law.

A typical high-speed generator is shown in Figure 5 and a cutaway view of a typical low-speed aircraft generator is shown in Figure 6. The cutaway view illustrates the methods of construction and ventilation, and also shows the multipolar design employed to permit the use of the maximum amount of active material without exceeding the specified diameter.

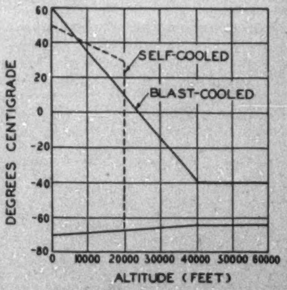

Figure 2. Standard Army–Navy maximum and minimum temperatures for altitudes to 60,000 feet

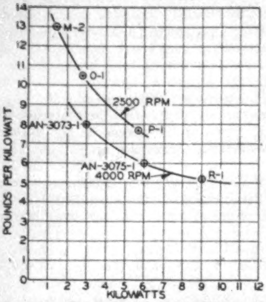

Figure 3. Weight–speed relationships for aircraft generators of current design

Service conditions involve linear accelerations of the order of 50*g*. These accelerations arise because engine-propeller combinations excite vibrations at various half-integral multiples of engine rotational frequency and also at other frequencies dependent upon the number of propeller blades and the gear ratio between the engine and the propeller. Common causes of vibration excitation are engine unbalance, engine reciprocating forces, uneven firing of the engine, and variations in propeller-blade thrust as the blades rotate through nonsymmetrical slip-stream velocities. Propeller-excited vibrations are expecially severe with the wide-blade propellers used for high-altitude service. Generator vibration is predominantly linear, although some torsional components are introduced by the off-center inertias of the leads and of the blast-tube connection. Destructive displacements are likely to result if one of the several critical frequencies of the generator coincides with an engine–propeller critical frequency. Calculated displacements for one make of type *P*-1 aircraft generator are shown in Figure 10. Examination of this curve will show that for some frequencies shaft displacements and housing displacements are in opposite phase. Such a condition is very likely to result in generator failure as a consequence of rubbing between armature and poles. Any detailed discussion of the requirements necessary to withstand vibration ambients is beyond the scope of this paper; perhaps it is sufficient to remark that the problems involved in designing a light-weight structure to withstand accelerations of 50 to 60*g* are not simple ones.

Operating experience and laboratory fatigue tests indicate that flanges for generators weighing 30 pounds or more must be made of forged steel, or of alloy-steel castings. They are attached by a large number of heat-treated bolts. Particular attention must be given to the design of the rim of the flange bracket to assure sufficient rigidity to prevent weaving and fatigue failure of the screws. Tool marks must be polished out and the finished brackets given a 100 per cent Magnaflux inspection. No design should be adopted until extensive tests have demonstrated its safe application limits.[2]

A resilient coupling is provided to absorb most of the torsional vibration present in the engine-take-off drive. The coupling must not fail during misfiring or other erratic operation of the engine, conditions which may cause alternating displacements of several degrees. The coupling used on the generators illustrated consists of synthetic rubber bonded to plates attached respectively to the drive shaft and to the armature. The performance record of this coupling has been excellent; such failures as have occurred have usually been the result of the shear limit deliberately built into the coupling to protect the engine gears against excessive torques. Another type of resilient coupling has been described in a recent paper by S. R. Bergman.[3] Other couplings in use are various forms of spring couplings, either with or without friction damping. Couplings which operate satisfactorily on main engines often give trouble when they are operated on auxiliary power units; this is particularly true of undamped spring couplings, and is a result of excitation frequencies other than those found on the main engine.

Ventilation

Adequate ventilation is essential; an ample supply of cold air must be provided, and, in addition, loss distribution, thermal gradients, transfer coefficients, and distribution of cooling air must all be balanced if the designer is to obtain maximum output for a given amount of material. Nowhere is the interdependence of mechanical and electrical design more forcefully emphasized. Air ducts must be located to minimize temperature differentials; they must not interfere with flux paths or with structural requirements; and they must carry the necessary volume of air with minimum pressure drop. Many three-kilowatt generators, and all generators of higher ratings, have axial-flow air ducts through the armature.

Early attempts to use integral fans for cooling three-kilowatt and six-kilowatt generators convincingly demonstrated some of the problems of self-ventilation at high altitudes; the air available for cooling the generator recirculated through the generator and through the already warm engine nacelle, so that the temperature of the air surrounding the generator became too high for effective cooling. This condition became progressively worse as the weight of air passing through the generator decreased with the decrease in air density at altitude. Generators threw solder, or burned up completely, and it was finally recognized that the larger generators would overheat at 30,000 feet from no-load losses alone.

Blast-tube cooling proved to be the solution of the problem, and has been

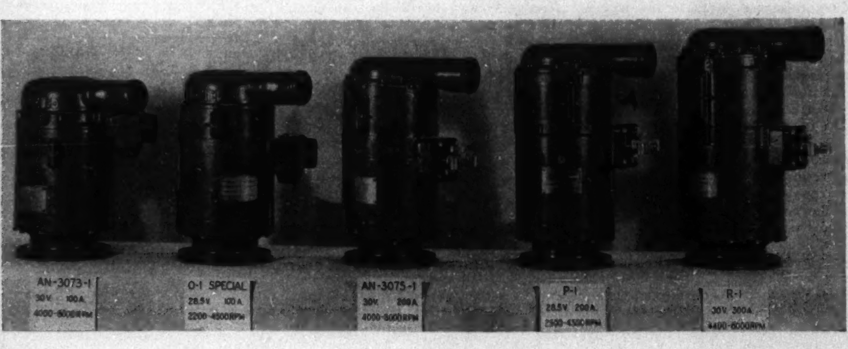

Figure 4. Typical aircraft generators

Machines shown range in rating from 30 volts, 100 amperes, to 30 volts, 300 amperes

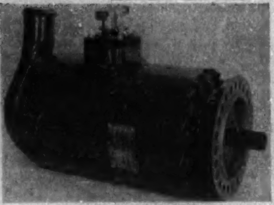

Figure 5. A 30-volt 300-ampere high-speed aircraft generator

specified for 100-ampere and higher ratings of generators since August 1942. Blast-tube cooling is accomplished by the installation of a ventilation duct which extends into the propeller slipstream where it makes use of slipstream velocity head to drive a considerable volume of outside air through the generator. With blast-tube cooling, low ambient temperatures at high altitudes (refer to Figure 2) will compensate to a considerable extent for the reduction in the weight of air passing through the generator in any given interval. This reduction is less than might be expected from density considerations, since an airplane must increase its minimum speed to maintain lift as air density decreases, and the increased speed increases the volume of air passing through the generator. Calculations indicate that a blast-tube-cooled generator of normal design will operate at a lower temperature at 30,000 feet than it will at sea level; actual tests confirm calculations. Minimum generator temperatures occur at some altitude in the region of 35,000 feet; by the time 50,000 feet is reached, generator temperatures will probably exceed values found at sea level, and will rise very rapidly for any further increase in altitude.

Efficient ventilation has a variety of beneficial results. The prevention of high temperatures is usually of consequence more because of the dependence of output on temperature than because of any actual danger from excessive temperatures to the insulation of a well designed generator. Operation at high temperatures is often advocated as a panacea for all rating problems but in some parts of a machine high temperatures are impractical. For example, increasing field-coil temperature from 125 degrees centigrade to 175 degrees centigrade will result in a reduction in excitation of 22 per cent; the crippling effect that low values of excitation have on generator output will be discussed in detail under electrical design. At high temperatures, not only does the magnetomotive force per watt of excitation power decrease; even the iron in the magnetic circuit requires increased excitation to maintain a given value of flux. High temperatures also reduce the mechanical strength of practically all materials, the effect on materials with low melting points such as solder being especially pronounced.

There is considerable room for improvement in ventilation of aircraft generators. Less pressure should be wasted in entrance and exit losses or in other frictional losses which do not aid in heat transfer. Thermal gradients should be reduced materially by improving heat transfer to the cooling air; a six-kilowatt generator usually has a maximum temperature rise in the neighborhood of 125 degrees with an air rise of about 75 degrees centigrade. A considerable increase in output would be permissible if the gradient from windings to air could be 25 degrees instead of 50 degrees; that is, higher losses and higher output-air temperature would be possible with no increase in winding temperature. Present installations in airplanes also leave much room for improvement. Friction loss in blast tubes should be reduced, and back pressure in the nacelles should be eliminated or reduced. Considerable work remains to be done in evaluating the constants for blast tubes, and making the results available to generator designers.[1]

Electrical Considerations

Aircraft generators resemble large industrial machines built in miniature and operated at extremely high overloads. Cost considerations are somewhat topsy-turvy; almost any expense may be justified if it will reduce the weight of a generator or increase the output obtainable from a generator of the maximum permissible weight, and, as a result, designers have freedom to exercise their ingenuity to the fullest extent. All existing generators, however, attest to the fundamental reasonableness of conventional electrical construction. Freak ideas have no place in generator design; only old and well tried principles, ingeniously applied, appear to accomplish much toward weight reduction.

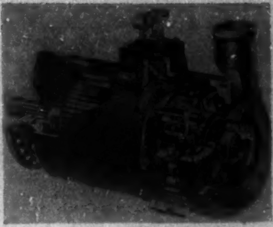

Figure 6. Cutaway view of a 28.5-volt 200-ampere low-speed aircraft generator showing method of ventilation

For aircraft generators, comparatively short life is required, and consequently, high temperatures are permissible, at least in the portions of the machine in which high temperatures do not cause an unreasonable reduction in output. Insulation which will last ten years at 105 degrees centigrade will last 2,500 hours at 145 degrees centigrade by the conventional rule that life is cut in half for each 8-degree centigrade rise in temperature.[6] Advantage may be taken of duty cycle in determining maximum allowable temperatures, as full-load operation is required for only a small percentage of the time a generator is in use. Glass and mica insulation can be used where temperatures of 175 to 200 degrees centigrade are necessary, although, in determining the ultimate life of a generator, the better mechanical properties of most class *A* insulating materials often more than compensate for the theoretically better high-temperature resistance of class *B* materials.

In contrast with the requirements for many industrial machines, noise is of no consequence for aircraft generators. Consequently, ventilation can approach tornado velocities, and magnetic densities can be anything for which excitation and cooling can be provided.

Minimum operating speed is the starting point for all generator-design calculations. No amount of ingenuity can wholly circumvent the law that, for a given amount of active material, operated at constant density in the magnetic material and at a fixed temperature of the

Table I. Principal Types of Aircraft Generators for 24-Volt Systems

Six-Stud Mounting—*AND*10002 Pad

Generator Type	Speed Range, RPM	Volts	Amperes	Kilowatts	Approximate Weight, Pounds	Blast Cooling
M-2	2,500/4,500	28.5	50	1.42	18.5	None
O-1	2,500/4,500	28.5	100	2.85	30	2-inch tube
O-1 spec.	2,200/4,500	28.5	100	2.85	34	2-inch tube
A N 3073-1	4,000/10,000	30.0	100	3.0	24	2-inch tube
P-1	2,500/4,500	28.5	200	5.7	44	2-inch tube
P-1 spec.	2,200/4,500	28.5	200	5.7	48	2-inch tube
P-2	5,000/10,000	28.5	200	5.7	35	2-inch tube
P-2 spec.	4,400/10,000	30.0	200	6.0	35	2-inch tube
A N 3075-1	4,000/10,000	30.0	200	6.0	36	2-inch tube
**R*-1	4,400/10,000	30.0	300	9.0	47	2-inch tube
Q-1	4,400/10,000	30.0	400	12.0	50	3-inch tube

* Actual generators will deliver 300 amperes at 4,000 rpm at a voltage between 28.5 and 30.

conductor materials, output is directly proportional to the minimum speed at which full voltage must be obtained. However, neither fixed density or fixed temperature need be conceded as design requirements. In fact, the attainment of maximum densities and minimum temperatures are basic objectives of the generator designer. The effect of both increased excitation and decreased temperature may be studied by referring to Figure 9. The curves show the maximum-output characteristics of a typical compensated generator. Voltage is held constant and field current is the maximum obtainable as field resistance varies with temperature. The high outputs shown in Figure 9 for extremely low temperatures are primarily the result of the moderate increase in excitation produced by low values of field resistance at low temperatures. Two conclusions are evident: if excitation power must be limited to a low value, low field temperatures are extremely beneficial in obtaining maximum outputs; and conversely, if high temperatures are going to prevail, excitation must not be arbitrarily limited.

Early aircraft-generator regulators were of the vibrating-contact type, and were somewhat overloaded at a nominal rating of 1.5 amperes. The upward trend in generator ratings which started in 1940 would have been impossible without corresponding advances in regulator capacity. Generator designers must share credit for producing 100- and 200-ampere generators with the voltage-regulator engineers who produced regulators capable of carrying a continuous field current of eight amperes.[4,5] Regulator ratings, however, have remained static while generator output requirements have doubled, and the time appears now at hand for the development of a regulator which will carry 15 to 20 amperes. The requirement that a 400-ampere generator operate with the same regulator which is used with a 50-ampere generator, and with 20 to 30 other assorted generators, imposes far too high a penalty on the 400-ampere machine. The advantages of a universal regulator are great, but ratings of generators cannot go on increasing indefinitely unless the regulator ceases to be the limiting factor it is at present.

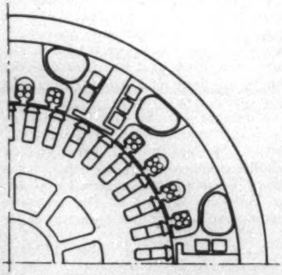
Figure 7. Quarter section of a six-pole aircraft generator with six interpoles and with pole-face windings

Combining heavy armature loading with relatively feeble excitation results in a tremendous amount of field distortion under load unless pole-face windings are employed. Compensating pole-face windings add considerable resistance to the armature circuit of a generator, but for the higher ratings, say those over 200 amperes, the resistance drop in the additional winding is usually much less than the loss in voltage which would be caused by field-form distortion in a similar machine without compensation.

Good commutation under all load and speed conditions requires the use of interpoles in generators of high ratings. The resistance of combined compensating and interpole windings, because of the better utilization of the available space when some of the interpole turns are placed within the pole face, is very likely to be lower than the resistance of interpole windings used without compensating windings. A further advantage of compensating windings lies in the fact that, except for small machines in which resistance drops predominate in determining maximum output, the overload capacity of compensated generators is higher than that of uncompensated machines, since the neutralization of armature cross field retards the collapse of excitation under heavy loads.

Although, on the largest machines, compensating windings are justified by these minimum-speed considerations alone, at high speeds compensation also materially improves commutation. At 4,000 rpm, a nine-kilowatt generator is permitted to have, by specifications based on present regulator limitations, a maximum excitation of 2.4 per cent of its output. At 8,000 rpm, the same generator must have its excitation reduced to well under one per cent of its maximum output to maintain rated voltage. Not only is compensation necessary to prevent shifting of electrical neutral and resultant bad commutation at high speeds and at loads up to rated full load; at speeds appreciably above minimum speed, a generator has tremendous overload capacity, and properly designed compensating and interpole windings will permit the delivery of astonishing outputs.

Commutating windings may also be used to produce a compounding effect. Generator designers, hamstrung at every turn by regulator limitations, are sorely tempted to provide a small amount of extra excitation by using interpole fields stronger than would be necessary for commutation requirements. Rotation of the armature in a strong interpole field generates voltages which cause circulating currents in the coils short-circuited by the brushes. These currents are in a direction to provide cumulative compounding, and the ampere turns produced are nearly proportional to speed and to load current. No compounding effect exists at standstill and therefore there is no danger of reversing generator polarity by closure of the reverse-current relay under this condition. Since compounding effect increases with speed, however, it is likely to cause instability at the high-speed end of the speed range unless limited to a small percentage of the total excitation. Better operation would be obtained by eliminating all compounding and using a regulator with adequate capacity to handle all of the excitation required.

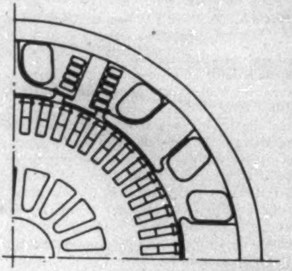
Figure 8. Quarter section of an eight-pole aircraft generator with four interpoles

The main poles are offset in pairs to provide space for the interpole windings

Current densities in windings and brushes of aircraft generators are naturally much higher than those permissible for industrial machines. Windings carrying load currents can be, and often are, worked at densities in excess of 10,000 amperes per square inch. Shunt-field-winding densities are determined largely by regulator rating, and are at present much lower than the densities used in armature, compensating, or interpole windings.

Brush densities usually exceed 200 amperes per square inch, and present indications are that, for the best over-all balance of design factors, densities approaching 300 amperes per square inch are desirable. High brush densities are necessary for acceptable weights; for maximum ratings they are absolutely necessary in order to allow the machines to be built within permissible dimension limits. The difficulties involved in making a large high-speed commutator have already been mentioned. If compromise is necessary, it is far better to compromise by raising brush densities than it is to make the commutator so large that it will distort under operating conditions.

The complex problems involved in the application of brushes to aircraft generators can be mentioned only briefly here

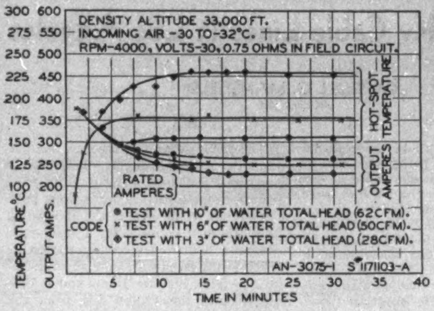

Figure 9. Altitude-chamber tests of a 30-volt 200-ampere 4,000–10,000-rpm aircraft generator showing the effect of ventilation on temperature and maximum output

Each test started cold at approximately −50 degrees centigrade. Full field, except for 0.75 ohm in field circuit. Load amperes adjusted to maintain 30 volts

Mechanical, electrical, and chemical phenomena combine to perplex the investigator. As recently as 1942, a pilot making a flight at altitudes in excess of 30,000 feet never knew when his generators would suddenly cease to function. Brushes which normally lasted 1,500 hours would disintegrate into a cloud of carbon dust, apparently spontaneously and nearly instantaneously. Under certain conditions, later, found to be associated with the lack of moisture and of oxygen at high altitudes, the brush–commutator interface developed excessive friction and the brushes ground away in a few minutes. Factors influencing wear are so critical that at least one case is on record in which one generator out of a complement of four landed with brushes completely worn out, while the remaining generators showed no brush wear at all. The solution of the problem of preventing brush wear at altitude is one of the fascinating stories of modern chemistry.

High-altitude brush wear proved to be almost independent of current density, although many investigators were misled into believing that low density was imperative if low brush wear was to be obtained, simply because some of the early attempts to reduce brush friction made use of lubricating ingredients which would not stand high temperatures. Once a treatment was found which would maintain an oxide film on commutators at temperatures of 200 to 250 degrees centigrade, it was found that brushes in well designed generators would operate at astounding densities.

Aside from high loadings, an aircraft generator usually differs from its industrial counterparts in having a greater number of poles. The number of main poles used is influenced by a variety of considerations, and various designers have chosen four, six, or eight poles to meet identical specifications. The advantages and disadvantages of a large number of poles may be summarized as follows:

Advantages:

1. Minimum magnetic section of frame and armature core required.
2. Better distribution of the brushes
3. Better distribution of the cooling air through field coils.
4. More room for ducts through the armature.
5. Shorter mean turn for armature coils.
6. Larger armature diameter possible for a given generator diameter.

Disadvantages:

1. More excitation required because of the higher total air-gap reluctance.
2. More field copper required, both because the higher air-gap reluctance, and because the reduction in mean turn is less than the increase in number of coils.
3. Poorer field-winding space factor because of the additional sets of insulation required.
4. Frame section may be determined by mechanical rather than by magnetic considerations.
5. The number of possible winding combinations becomes more restricted as the number of poles increases.
6. It is often difficult to secure sufficient space for brush holders.
7. Brush and pole spacing become extremely critical because of the small number of mechanical degrees for an excessive error in electrical degrees.
8. Iron loss is higher.

Evaluation of these factors for each particular case shows that the use of six poles is usually preferable for machines of the dimensions and ratings under consideration, although in some cases the use of eight poles will result in lower weight.

Closely connected with the choice of number of poles is the choice between wave-wound, or series-connected armatures, and lap-wound, or parallel-connected armatures. Armatures rated 200 amperes or less are almost invariably wave wound; armatures for 300-ampere machines may be either wave wound or lap wound; and armatures for larger ratings should probably be lap wound in order to obtain a sufficient number of slots per pole. In general, full-pitch lap-wound armatures have a narrow neutral zone and will operate satisfactorily with thicker brushes than can be used with wave-wound armatures. They have the disadvantage of requiring cross connections, and usually they will have a poorer space factor than wave-wound armatures because of the additional turns required. Lap windings are necessary whenever a one-turn wave-wound armature develops too much voltage with minimum number of slots per pole. A minimum of six slots per pole is desirable, although machines have been built with as few as four slots per pole. Machines with a small number of slots per pole have excessive flux pulsations and also have extremely wide commutating zones. Theoretically at least, a considerably wider pole span is feasible with a full-pitch lap-wound armature than would be possible with a wave-wound armature designed for equal voltage.

There is little difference in the amount of copper that can be wound into lap-wound or wave-wound armatures, but what difference there is is in space factor and it favors wave windings. Even with a wave winding with two rectangular conductors per slot, space factor is rarely much higher than 50 per cent. Here then is a promising field for improvement. A typical slot for a 200-ampere machine is 0.11 inch wide; 73 per cent of this width is used for copper, 12 per cent for conductor insulation, and 15 per cent for slot insulation and punching allowances. This same slot is 0.448 inch deep; 72 per cent of this depth is

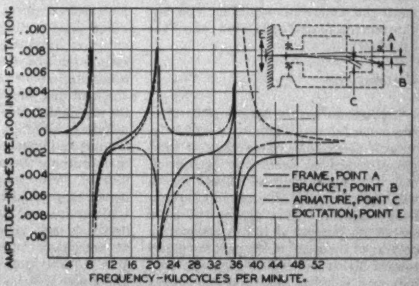

Figure 10. Amplitude–frequency curve for a typical engine-mounted generator system with three degrees of freedom

No damping assumed

used for copper, 6 per cent is used for conductor insulation, and 22 per cent is required for wedging, slot insulation, and other allowances. These figures indicate a total space factor of 53 per cent; the radii on the edges of the conductor reduce the actual figure to 52 per cent. A moderate amount of improvement could be realized by developing a conductor insulation which would not require any additional slot protection, perfectly aligned smooth punchings, and stronger, thinner wedges than are now available would provide still more gain. A possible increase of 20 per cent in the amount of copper in the slot, or a space factor of 60 to 65 per cent in place of 50 to 55 per cent, would appear feasible as improved insulating materials are developed.

Any change in conductor material does not appear likely at present. Silver is the only conductor which is better than copper, but it does not appear feasible to use this material for a gain of only five or six per cent. Light-weight materials are utterly out of the question for electrical windings; the resistances of all such materials are far too high.

The use of high-induction magnetic alloys offers some attractive possibilities. It has been known for many years that the addition of substantial amounts of cobalt to iron will increase saturation flux by 10 per cent or more and will increase permeability at moderate magnetizing forces by 30 to 50 per cent. Cobalt is both expensive and scarce, however, and the difficulties of processing cobalt-bearing iron are considerable. Even if such high-induction material were available, its use would not produce the same increase in total flux that could then be permitted in flux density in the iron portions of the machine, since air-gap reluctance consumes 50 to 70 per cent of the total excitation required for operation at minimum speed. Until higher-power regulators become available, little gain in output can be effected by simply substituting high-induction iron for the usual lamination stock. Special proportions of iron and copper will not solve the problem, as the material is so brittle that it is not feasible to use narrower teeth; moreover, narrower teeth and wider slots would increase air-gap reluctance and reduce or cancel the expected gain.

Conclusions

An attempt has been made to cover in one paper the major requirements for d-c aircraft generators. Treatment of individual topics has necessarily been brief, and it is hoped that additional papers will be forthcoming on many of the problems outlined. This paper will have served its purpose if it has adequately portrayed the trend of the past few years toward higher and higher generator ratings, and if it has suggested approaches to solutions of some of the problems involved as further increases in ratings are necessary.

The points made may be summarized in a few words: High maximum ratings for aircraft generators are of primary importance to military aviation. In general, high ratings will be and must be accompanied by high operating speed. As far as mechanical considerations are concerned, endurance, especially at high speeds, is of primary importance, while improvements in ventilation offer a major opportunity for increased ratings. As far as electrical considerations are concerned, increased excitation will do more than any other single factor to increase output at any given speed. Generators for maximum ratings must be designed to provide good commutation over the full range of speed and load, and, in general, this requires the use of carefully designed interpoles and pole-face windings. High temperatures are not as important in obtaining maximum outputs as has sometimes been indicated, but temperatures are much higher than could be tolerated for industrial service, and it is necessary that the materials used have adequate endurance at maximum operating temperatures. It is probably more important that insulating materials be improved in mechanical properties, especially those determining space factor, than it is that they be improved in temperature resistance.

Two things are suggested for the consideration of those who would like to assist the generator designer in his efforts to increase ratings:

1. The intensive development of high-induction magnetic materials.

2. The development of a voltage regulator which will permit field currents of at least 15 amperes.

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New 138-Kv Cable Lines in Los Angeles

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Synopsis: The first 138-kv underground transmission cables to be installed west of Chicago and the largest now in use at this voltage (except for short station runs) were placed in service during 1943 by the Los Angeles Department of Water and Power. While generally similar to 138-kv lines previously installed in the East this installation involves a number of novel design features which make it of more than usual interest. The paper describes these features and some of the studies of carrying capacity, cable movements, sheath voltages, oil supply and alarm, and sheath corrosion, which led to their development, and includes a summary of significant data and characteristics of the installation.

THE 138-kv underground-transmission cable lines recently placed in service in Los Angeles, comprising about $12^1/_2$ miles of circuit, form parts of the 138-kv loop system which ultimately will encircle the central part of the city and connect together all of the receiving stations, at which lines from remote power sources terminate, and the local generating stations. Their place in this main system is shown in Figure 1.

The principal unit in the new installation is a double-circuit underground line about five miles long, extending from new receiving station *D* north along Fairfax Avenue to the edge of the Hollywood hills, where it connects to a seven-mile overhead line from receiving station *E*, which is the terminus of the third 287.5-kv line from Boulder Dam, and is also tapped into the 110-kv line from the San Francisquito plants of the Los Angeles Aqueduct. Lightning arresters and hand-operated disconnecting switches are installed at the junction between overhead and underground portions of the line.

The remaining $2^1/_2$ miles of underground circuit connects the new harbor

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The design and installation of these lines were made under the active supervision of H. C. Gardett, M. O. Bolser, and C. H. Jenkins. The assistance of Boris A. Gray in preparation of data is acknowledged.

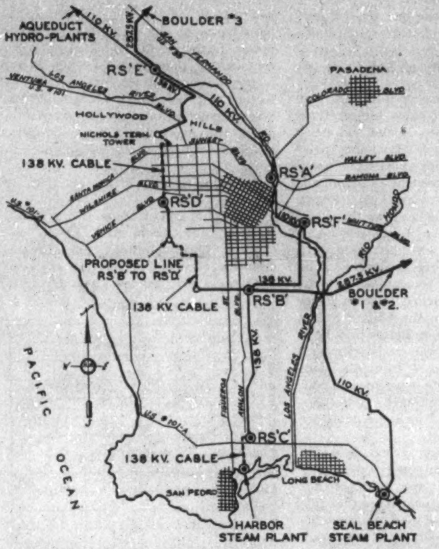

Figure 1. Map showing location of underground-cable lines in 138-kv loop system of Los Angeles Department of Water and Power

——Overhead lines
- - - -Underground cables

steam plant to receiving station *C*, at the southern end of the system. Duct and manhole space has been provided for a second circuit here, and the cable will be installed when the second generating unit is added. At receiving station *C* the line connects to open busses through the usual type of outdoor terminals and oil switches. At the steam plant, however, each cable enters an oil-filled disconnecting and grounding compartment attached to the transformer, through a special type of entrance terminal. The generator, transformer bank, and cable circuit are operated as a unit.

The line between receiving stations *B* and *D* will be added as soon as materials can be obtained and will include a 5.3-mile underground section where it passes through a residential area. The cable installation will be substantially the same as those now in use.

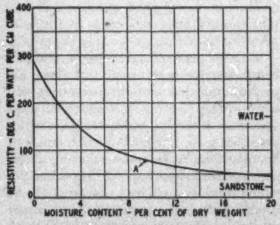

Figure 2. Thermal resistivity of soils

A—Apparent resistivity of sand from tests on experimental conduit, for use with equation 4

The enviable operating record already established by oil-filled paper insulation in high-voltage cables, together with the sheath troubles experienced on cables of all voltages, suggests that the useful life of these cables easily may prove to be fixed by the sheath rather than by the insulation. Considerable attention accordingly was given, in the design of this installation, to all available means of prolonging the life of the sheath. The rather generous bend space provided in the manholes and a number of the novel features incorporated in the installation have this as their principal objective.

Transmitting Capacities

The transmitting capacity required in lines of this loop system is fixed at 125,000 kva per circuit by the requirement that, when the system is fully developed and loaded, each pair of circuits shall be capable of carrying the entire load distributed through one receiving station.

The procedure followed in analyzing the effects of conductor size, insulation thickness and power factor, cable arrangement, and soil characteristics, on transmitting capacity, was to calculate the capacities for several conductor sizes with selected groups of other conditions and then study the characteristics of the resulting families of capacity curves. In each such calculation, the temperature rise due to the dielectric loss was first deducted from the total allowable rise, leaving the rise available for load losses. The thermal resistances applicable to load losses were then summed up, with suitable adjustments to reflect the effects of load factor and sheath losses. The allowable load current is then

$$I=\sqrt{\frac{T_L\times10^3}{R_{ac}\times R_{th}}}\ \text{amperes} \tag{1}$$

in which T_L is the temperature rise due to load losses, in degrees centigrade; R_{ac} is the effective conductor resistance, corrected for skin and proximity effects, in ohms per thousand feet; and R_{th} is the adjusted thermal resistance for load losses, in degrees centigrade per watt loss per foot of cable.[1]

The maximum summer ground temperature to be expected is not over 25 degrees centigrade, which, for an allowable copper temperature of 70 degrees, leaves a rise of 45 degrees centigrade available for cable losses. During the winter months (December–April) the ground temperature is about seven degrees lower, and the available rise is 52 degrees. All calculations were made on the summer basis.

Thermal Resistances

The path of heat flow divides naturally into four principal elements; the cable paper, the sheath surface and duct air, the conduit structure, and the soil. Thermal resistances of the cable paper, sheath and duct air, and soil, all expressed in degrees centigrade per watt loss per foot of cable, are given by the following equations:

$$\text{Paper } R_{th}=0.012\,\rho_1\log\frac{D_1}{d} \tag{2}$$

$$\text{Sheath } R_{th}=0.00411\,\frac{B}{D_2} \tag{3}$$

$$\text{Soil } R_{th}=0.012N\,\rho_2\log\frac{r}{\alpha} \tag{4}$$

where

ρ_1 = thermal resistivity of cable paper, taken as 550 degrees centigrade per watt per centimeter cube
ρ_2 = thermal resistivity of soil in degrees centigrade per watt per centimeter cube (see Figure 2)
B = surface thermal resistivity of cable sheath, taken as 1,200 degrees centigrade per watt per square centimeter
d = conductor diameter in inches
D_1 = outside diameter of cable paper in inches
D_2 = outside diameter of cable sheath in inches
N = number of equally loaded cables in conduit
r = radius of equivalent isothermal cylinder representing conduit surface, in inches
$\alpha = r^2/(2n-\alpha)$, displacement of the origin, or focus, of the lines of heat flow from the conduit axis, in inches
n = distance from ground surface to axis of conduit, in inches

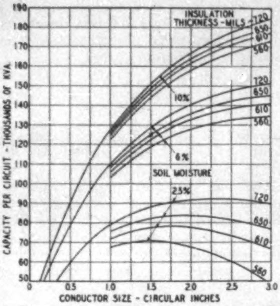

Figure 3. Transmitting capacity of 138-kv cable lines

Two circuits in conduit (Figure 6), with four-foot cover. Load factor, 75 per cent; dielectric power factor, 0.75 per cent

No attempt was made to calculate the thermal resistance between the duct wall and the conduit surface by purely theoretical methods. The value used was derived from data obtained in an extensive series of tests made with both constant and varying loads in a span of experimental conduit containing 16 four-inch fiber ducts and 16 temperature-measuring units attached to cable sheaths or embedded in the conduit structure. To the resistance measured in these tests was applied a series of corrections based on relative dimensions of the heat-flow paths in the two conduits. The resistance so found for the conduit section shown in Figure 6 was 1.48 degrees centigrade per watt loss per foot of cable.

The somewhat simpler scheme of using standard "duct constants" to calculate the temperature rise in conduit and soil was not satisfactory for this study, because it was desired to trace the effects of differences in duct arrangement, conduit depth, and soil characteristics.

Equation 4 is derived from parallel-cylinder image theory and is exact for heat flow from a cylindrical conduit to the ground surface if both are isothermals. Some error may be introduced in choosing the equivalent cylinder to represent the actual conduit surface, but it is believed that any such error is largely neutralized by using the same procedure in deducing the effective soil resistivity from test data on other similar conduits. The equivalent cylinder used is one having the same cross-sectional area as the conduit.

Thermal resistances of the soil calculated from equation 4 were increased by two per cent to compensate for the fact that the conduit surface is not a true isothermal. The correction is based on comparison of sheath temperatures in top and bottom ducts in the experimental conduit in relation to duct spacing and conduit depth. The effect is to base the load rating of the line on the temperature of the cables in the bottom ducts.

Thermal Resistivity of Soil

The greatest source of uncertainty in these calculations of cable temperatures and carrying capacity is the soil resistivity ρ_2 in equation 4. Its value varies over a wide range with soil texture, packing, and moisture content, and reliable data regarding these variations are very meager. Resistivities used in these calculations were taken from the curve shown in Figure 2, which is based on a study of data from several sources.[2] The shape of the curve is similar to that found for loosely packed Hudson River sand in the tests by the United States Bureau of Soils,[3] but ordinates have been reduced to match the resistivity calculated by equation 4 from the constant-loss tests on the experimental conduit previously mentioned. This conduit was in fine sand having a moisture content of 9.4 per cent.

A load cycle having a loss factor of 62.5 per cent was adopted for calculation purposes, and it was assumed that the resulting temperature rises in the cable insulation and duct air would be the same as with a constant load of the same peak value, while those in the conduit and soil would be lower. It was found in tests with similar load cycles on the experimental conduit that the ratios of the temperature rises in conduit and soil to those with constant loads were appreciably greater than the loss factor. The difference appears to vary with the position of the peak in the load cycle, but, for the symmetrical load cycle used, the ratios found were 0.75 for the conduit structure and 0.70 for the soil. The effect is taken into account by multiplying the thermal resistances of the conduit and soil by these factors in the calculations involving load losses.

Since the lines are fully equipped with bonding transformers, the sheath loss due to circulating currents was assumed to be negligible. There remains, however, a small loss due to local eddy currents resulting from proximity effects. This loss is proportional to the square of the load current, and its effect is most conveniently taken into account by increasing the thermal resistances of sheath surface, conduit structure, and soil, when used in calculating load loss temperature rise, in the ratio of total copper and sheath losses to the copper loss alone. The effect is not a large one. In the range of conductor sizes here considered, the increment varied from one to eight per cent, and has a value of 2.2 per cent for the cable size and arrangement actually used.

In making these calculations, any tendency toward cumulative loading of the various steps with safety factors "to be on the safe side" has been avoided. The load rating of the lines, however, has been based on the guaranteed dielectric power factor of the cable of 0.75 per cent, rather than on the average power factor of about 0.40 per cent as shown by factory test, because it was felt that the possibility of an increase in power factor due to the effects of installation or operation has not been wholly eliminated.

Conductor Size

Figures 3 and 4 show the principal results of the transmitting capacity calculations previously described. The undesirable effects of using thin cable insulation in dry soil are strikingly illustrated in the lower group of curves in Figure 3. Not only does the capacity decrease rapidly with reduction of the insulation thickness, but for each insulation thickness there is a definite limit to the amount of power which can be transmitted over each circuit, regardless of conductor size. This limitation results from the fact that, under the conditions assumed, the greater part of the total temperature rise is produced by the dielectric loss, and the increase in this loss due to the paper volume added as the conductor is enlarged causes a disproportionately large reduction in the temperature rise available for load losses.

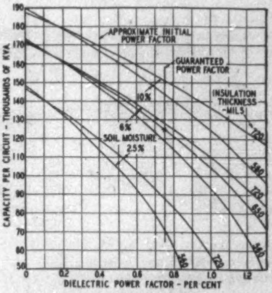

Figure 4. Effect of dielectric-power-factor changes on transmitting capacity

Conductor size, 1,500,000 circular mils; load factor, 75 per cent

With six per cent soil moisture, which is about the minimum expected along the present lines, this limitation does not occur within the range of practical sizes. The cross indicates the cable selected for these lines; a 1,500,000-circular-mil conductor with 650 mils of insulation, giving an estimated capacity of 125,000 kva per circuit with two circuits operating in one conduit line. The capacity of one circuit operating alone is about 165,000 kva. Table I shows how the total available temperature rise of 45 degrees centigrade is distributed under both conditions.

The seven-degree reduction in ambient temperature during the winter months increases the calculated capacity by 13 per cent with both circuits operating, and by 11 per cent with only one circuit in use.

Figure 4 shows the effect of dielectric-power-factor variations on transmitting capacity. Again the undesirable effects of dry soil and thin insulation are evident, although the effect of the latter is greatly reduced if the power factor remains low. Thus, if the dielectric power factor remains at 0.40 per cent, the summer capacity of the line with six per cent soil moisture will be 149,000 kva per circuit.

An insulation thickness of 650 mils was chosen instead of the minimum recommended thickness of 560 mils, principally because, in view of the importance of reliability, it was desired to incorporate a substantial factor of safety against electrical failure. This increased the total cost of the line by about eight per cent. But, as can be seen in Figure 3, it also increases the capacity by five per cent, under the standard conditions of 0.75 per cent dielectric power factor and six per cent soil moisture, so that the increased safety resulting from lower electrical stresses is obtained at a net cost of only three per cent. It is interesting to note that if this cable were installed in soil having a moisture content of $2^1/_2$ per cent, the use of the thicker insulation would increase the line capacity by 16 per cent and the cost per kilovolt-ampere of capacity actually would be less than with the minimum insulation.

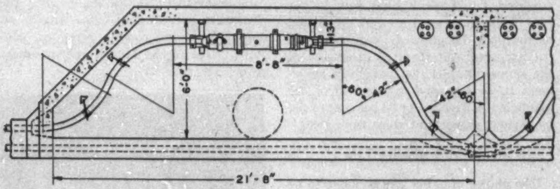

Figure 5. Plan of 138-kv stop-joint manhole, showing arrangement of power cable and joint

Oil supply, bonding connections, and other accessories omitted

Manhole and Conduit Layout

The two circuits are kept as nearly independent as possible by using the double manhole system, and by providing somewhat greater separation between ducts occupied by cables of different circuits than between those of each circuit group. The tandem manhole arrangement shown in Figure 5 was adopted because of its convenience and effective utilization of the street space occupied, particularly as to width. The ducts occupied by each circuit in the front wall enter their respective manhole compartments through horn-shaped recesses which are displaced vertically to permit crossing without interference at the center wall. The only connections between the two compartments are two three-inch openings in the center wall for the signal cable and a connection between the neutrals of the bonding transformers.

Manholes are built of reinforced concrete, placed over steel forms, and pulling irons and the larger equipment-mounting bolts are cast into the walls. Expansion bolts in drilled holes are used for lighter attachments. One pulling iron is placed in the floor directly below the manhole head, and others are placed in the end walls opposite each duct mouth. The pulling line passes vertically down to a pulley in the floor iron, then diagonally upward to a second pulley in the proper end-wall iron, and horizontally into the duct at the opposite end of the manhole. This arrangement requires a minimum of special setup and makes it possible to pull the cable end across almost the entire length of the manhole in one operation.

The normal conduit section consists of six $4^1/_2$-inch fiber ducts, arranged in a circle around a three-inch signal cable duct and embedded in concrete as shown in Figure 6. The three-inch duct is raised slightly above the center of the duct circle to place the signal cable as close as possible to the center of the power cable group. Precast concrete spacers are used to hold the ducts in exactly the desired positions while the concrete is being placed. The change from this section to the 20-inch vertical spacing required in the manhole is made in a standard 20-foot "transition section," for which three special spacers are provided. Each duct terminates in a two-foot length of thick-walled fiber, the inside surface of which is flared out along a radius of about six inches in the exposed end to eliminate the danger of sheath cutting at this point.

Curved fiber of 65-foot radius is used on curves, and five-foot radius bends of six-inch fiber are used for risers at the terminals. All completed ducts are checked for clearance with a 12-inch steel mandrel, 0.25 inch smaller than the nominal duct size, and are cleaned by pulling through a special train of alternating steel-wire-disk brushes and grease-lined cups to remove dirt and pebbles or other foreign particles.

The standard span length was fixed at a conservative value of 710 feet to keep the cable movements, induced sheath voltages, and static oil head on grades, well within safe limits, and because this length proved most effective in keeping the manholes away from street intersections and other obstructions in the first group of lines built. Nominal half spans, 350 feet long, were used at all 90-degree turns in these lines to reduce pulling stresses. These stresses proved to be so moderate, however, that full-length spans will be used at corners on future lines.

Cable Movements

Two ranges of cable temperature are of particular importance because of their effect on cable movements. The first is the daily variation which causes the daily cycle of movement chiefly responsible for the development of fatigue cracks in the sheath and so determines the bend arrangement necessary to insure reasonable sheath life. This range was estimated to be about ten degrees centigrade from a study of the observed temperature variations during load-cycle tests on the experimental conduit.

The second significant temperature range is the maximum to be expected during the life of the cable, which determines the total joint movement to be provided for in the manhole and places a lower limit on the bend radius which may be used. This temperature range, for the present installation, is 60 degrees centigrade, since the maximum copper temperature is 70 degrees and the minimum probable ground temperature is ten degrees.

The cable movements which will result from these temperature changes

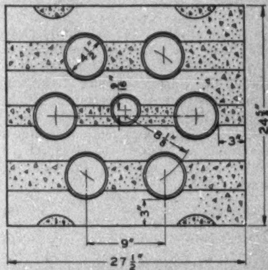

Figure 6. Cross section of conduit for two 138-kv-cable circuits, showing use of precast spacers

Three-inch duct holds signal cable at center of power-cable circle were calculated from the formulas given by Halperin,[4] which take into account the forces of tension or compression which must be developed in the cable to cause movement against the duct friction and the reaction of the cable bends in the manhole. Briefly, the result of these restraints is that small temperature cycles cause movement only at the ends of the span. As the temperature range is increased, the movement extends farther into the duct and for a certain "critical" range reaches the middle of the span.

These movement formulas were developed on the assumption that the direction of the bend reaction reverses during each half-cycle of motion so that it opposes the movement in both directions. But where, as in the present installation, the bends are large and the daily movement small, the bend reaction usually will remain in the same direction and nearly constant in magnitude throughout the cycle because of the elastic characteristics of the cable. Under these conditions it is probably more accurate to neglect the bend reaction. In the movement due to the larger temperature range, reversal of this reaction may occur, but its effect on the total movement probably does not exceed two or three per cent. On omitting this term and introducing the numerical values of the constants involved, the working equations for a 1,500,000-circular-mil cable, weighing 17.1 pounds per foot, assuming a coefficient of friction of 0.5 between cable and duct, reduce to the following forms:

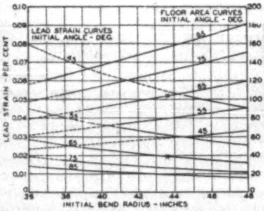

Figure 7. Effects of initial radius and angle of expansion bends on lead strain due to daily movement, and on required manhole area

Daily cable movement, 0.20 inch
Total cable movement, 4.3 inches

$T_c = 28.9S$ (5)

$M = 0.00346T^2$ for $T < T_c$ (6)

$M = 0.2ST - 2.9S^2$ for $T > T_c$ (7)

$d = 17.3T$ for $T < T_c$ (8)

in which

T = temperature change (copper) in degrees centigrade

T_c = critical temperature change necessary to produce maximum possible change in stress throughout the cable

M = total movement of cable in inches (both ends)

S = duct length in thousands of feet

d = distance in feet which the cable movement extends back into the duct

Since the actual duct length in a normal span is about 690 feet, the critical temperature range is, from equation 5, 20 degrees centigrade. The movement due to the daily cycle of ten degrees centigrade, therefore, is given by equation 6. If the free expansion of the exposed cable in the manhole is added, the increase in cable length in each end of the manhole is found to be 0.20 inch, or about 28 per cent of the free expansion of a half span of cable. This illustrates a beneficial effect of the duct friction which in this case reduces by more than two thirds the movement available for production of sheath-fatigue damage.

The distance to which this motion penetrates toward mid-span is 173 feet, from equation 8. This means that about half the length of the span remains undisturbed by the daily cycle, and the probability of creepage troubles is small. It also suggests that some tendency to creep may appear in half-length spans, particularly where installed on a grade.

The total movement due to the 60-degree temperature range is given by equation 7. If we assume that 60 per cent of this movement appears at one end of the span because of irregularities in friction, and add the free expansion of the exposed cable, the maximum movement to be expected in one end of the manhole is 4.3 inches, or 83 per cent of the movement if restraint were negligible.

Lead Strain and Sheath Life

The choice of cable arrangement to be used in the manholes was based principally on a study of the small alternating strains produced in the lead sheath by the daily cycle of cable movement, since fatigue cracks resulting from these strains are a frequent cause of sheath failure.

If the cable between the duct mouth and the end of the joint is formed into two bends of equal radius, the change in radius and angle of the bends and the movement of the joint may be calculated readily on the assumption that the cable entering the manhole remains straight and the bend radii decrease uniformly. The amount by which the lead sheath is strained then may be found from the change in either the radius or angle of the bends. In terms of the change in angle, the equation for lead on the outside of the bend is

$$\text{Lead strain} = \frac{100r}{R+r} \times \frac{\theta_1 - \theta}{\theta} \text{ per cent} \qquad (9)$$

in which r and R are the cable radius and initial bend radius respectively, and

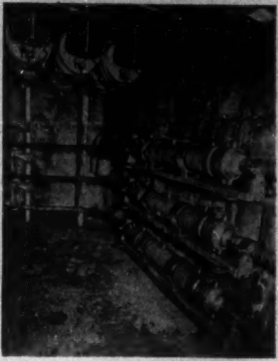

Figure 8. Typical stop-joint installation

θ and θ_1 are the bend angles before and after the movement.

The lead strains so calculated for the expected daily movement of 0.2 inch and for bends having a number of different initial radii and angles are shown in Figure 7 (curves sloping upward to left). The second group of curves in Figure 7 shows the variation in floor area of the manhole required to accommodate these bend arrangements if the total range of cable movement is 4.3 inches and the clearances and straight cable lengths required for joint assembly remain constant. The areas shown are averages

Figure 9. Cable terminal mounting and risers showing electrolytic grounding cell, counterbalance unit, and hood over duct mouth

of those required for stop and normal joints in each case. The dotted portions of the curves represent unusable combinations of initial bend radius and angle which result in reduction of the bend radius to less than the allowable minimum of about 35 inches when the cable is fully expanded.

The conditions selected for this installation are indicated by crosses on the 65-degree curves in Figure 7. Because of the difficulty of training cable of this size with a true point of inflection, however, a short straight section was introduced between the two curves by reducing the bend angels to 60 degrees without changing the radius or total offset. The motional characteristics are not appreciably affected.

While the relation between lead strain and sheath life is not well established, some idea of probable sheath life in this installation may be formed by comparison with the results of a series of accelerated sheath-life tests on two typical cables reported by Halperin.[4,5] The minimum life indicated for commercially pure lead sheath under the test conditions was in each case 9.6 years, and the average was about twice as great. The lead strain calculated by the afore-mentioned method on the basis of some reasonable assumptions as to unspecified bend details is in each case about 0.32 per cent, while the corresponding strain for the daily cycle in this installation is 0.02 per cent. If we assume the life to be inversely proportional to lead strain, the indicated minimum sheath life for this cable is, therefore, about 153 years.

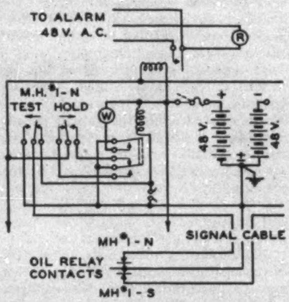

Figure 10. Schematic diagram of oil signal system showing connections of one annunciator unit

As is well known, however, the change in bend radius due to cable movements is not uniform as assumed in the foregoing, but tends to concentrate in the regions immediately adjacent to the end of the joint and the duct mouth. The effect on sheath life may be evaluated roughly by considering the extreme case in which the bends are assumed to remain rigid and to act as though hinged at each end. This rigid bend theory does not lend itself to direct calculation of lead strains, but a comparison of relative strains may be based on the assumption that they are proportional to the product of cable radius and the change in the angle between the duct axis and the chord of the bends. The lead-strain "factor" calculated for the daily cycle on the present cable by this method is about 13.5 per cent of the corresponding "factor" for the accelerated life tests previously mentioned. This indicates a minimum sheath life of 71 years, or a little less than half that found by the other method.

Such comparisons are open to many obvious objections, and the spread in results is fairly indicative of the uncertainties involved. But since the two cases considered represent perfect distribution of bending on the one hand and extreme concentration on the other, it seems reasonable to expect that fatigue troubles will not be serious during a period at least approaching the lower figure.

Some consideration was given to the suggestion[6] that the joint motion be restrained to obtain a more uniform bending of the cable. To prolong sheath life effectively, this control would need to be in operation during at least a major portion of the daily cycle of movement which is chiefly responsible for sheath fatigue. Actually, since the daily movement in this case is only 0.2 inch, the resulting lateral motion simply will cause the cable to move from side to side in the duct, and any restraint of the joint would be ineffective so far as the daily cycle is concerned, unless the possibility of such lateral motion at the duct end be practically eliminated.

Joint Movement

The lateral movement of the cable joint, due to the full temperature range, may be calculated from the corresponding longitudinal movement of 4.3 inches by either of the methods discussed in connection with lead strains. The assumption of a uniform change in bend radius indicates a joint movement of 4.2 inches, while the rigid-bend theory gives 6.2 inches. The actual joint movement will be somewhere between these figures and for design purposes was taken as 5.5 inches.

The cable was installed at a temperature about ten degrees centigrade above the assumed minimum and is presumed to have been left in a state of maximum tension. Contraction produced by cooling through this ten-degree range therefore would follow the unmodified copper bar law (first term of equation 7) and would be about 20 per cent of the cable movement caused by the full 60-degree range. Movement of the cable joint toward the manhole wall from its initial position therefore will be 80 per cent of the total joint movement, or 4.4 inches.

Figure 11. Attaching 1,500,000-circular-mil cable connector with four-ram 20-ton hydraulic press

The joint axis was placed 13 inches from the wall, which in the case of the largest joint, allows a free movement toward the wall of six inches. The length of rack arm used also allows a four-inch movement away from the wall, although the probability of the joint ever moving into this region seems remote.

Induced Sheath Voltages

It seems well established that differences of more than 11 volts alternating current between cable sheaths and

Figure 12. Oil-reservoir hoist

ground may cause corrosion under some conditions.[4] Since freedom from such trouble was a major objective in the design of this line, the possibility of reducing this voltage by modification of the duct arrangement was studied with some care. It was also desired to locate the signal cable so that the voltages induced in it would not impair seriously the operating characteristics of the alarm system. The following formula for induced voltages was found most useful in this work:

$$\boldsymbol{E}_s = 4.58\left\{\log\frac{S_2S_5}{S_3S_6} + j0.577\log\frac{(S_1S_4)^2}{S_2S_5\,S_3S_6}\right\}$$

volts per 100,000 ampere-feet (10)

wherein each S represents the distance from the axis of the conductor indicated by its subscript to that of the parallel conductor for which the induced voltage is being calculated. When this latter conductor is a sheath concentric with one of the circuit conductors, this axial distance becomes zero and is replaced by the mean radius of the sheath, r_m.

Equation 10 is derived with the current in conductor 1 as the reference vector. Some modification is necessary if either load or power factor in the second circuit differs from that in the first. As is evident from the symmetrical arrangement of subscripts, the induced voltage with only one circuit in operation may be obtained by simply dropping the S factors referring to conductors of the second circuit, while the effect of additional circuits may be included by adding the corresponding distance factors in each term of the equation.

Studies of the effect of cable arrangement on induced voltages lead rather directly to a circular arrangement of the circuit cables with the signal cable at their center, as the ideal grouping. This signal-cable position is particularly advantageous, since it is the only one at which the induced voltage is zero for both double- and single-circuit operation and for any fault current which returns on the cable sheaths.

It will be observed that the circular arrangement actually used, and shown in Figure 6, is not a hexagonal one. The outer phases of each circuit have been moved along the circle from the hexagonal positions to give greater separation between the two circuits. This slightly increases the sheath voltage with both circuits operating, but decreases the higher value which occurs with only one circuit in use. The effect is shown clearly in Table II, which also gives the sheath voltages for a rectangular arrangement having the same phase and circuit spacings as Figure 6. In all cases, the cables are arranged with A phase of one circuit adjacent to C phase of the other, and only two sheath voltages are given since the others have the same numerical values.

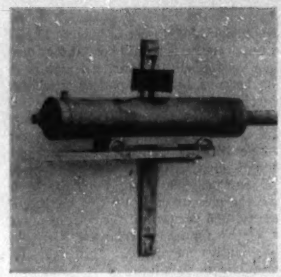

Figure 13. Use of condenser carrier in assembling stop-joint condenser unit

The maximum sheath voltages induced in each half of a normal 710-foot span, calculated from equation 10, and including the effects of the modified cable arrangements in the manholes and adjacent transition conduit sections, are 6.8 volts with 125,000 kva on each circuit, and 11.5 volts with 165,000 kva on one circuit only. These values are reduced about five per cent if the effect of the change in cable arrangement at the manholes is neglected.

This change in cable arrangement also results in some voltage being induced in the signal cable. This voltage is cumulative throughout the line and is greatest with only one circuit operating. It is expected to reach a maximum value of about 35 volts in the Fairfax line. Operation of the signal circuits is not affected, since the voltage appears only between conductors and ground, but it would be troublesome if a ground return were used for any of the signal circuits. The sheath voltage is suppressed by grounding in each manhole.

In addition to the induced voltages already discussed, there may be another voltage set up between cable sheaths and ground by the charging current of the cable flowing through the bonding transformers, if leakage resistance in the ducts is not too low. The changes in resultant voltage due to these two effects under varying load conditions are not easily traced. But since the exciting current of the bonding transformer at 12 volts to neutral is about 17 amperes, it is evident that the charging current, which is about one ampere per half span, can have only a minor effect at that voltage, whatever its phase relation to the induced voltage may be. At lighter loads, the charging current becomes relatively more important and sets a lower limit below which the resultant voltage between sheath and ground cannot be reduced at both ends of the span simultaneously. On the present line this limit is about five volts. The results of tests made to check induced-voltage calculations may be quite puzzling if this effect is not recognized.

Sheath Transients

Switching operations on lines of this kind frequently set up rather high transient voltages between the cable sheaths. Any open sparks from this source were particularly undesirable in the present installation, because of possible accumulations of stray natural gas in the manholes where the line passes the sites of many abandoned oil wells. An enclosed multiple spark gap therefore was developed to localize and confine any such sparking. It consists of six electrodes connected to the cable-sheath ends, mounted around a grounded hexagonal center electrode, under a Pyrex cover gasketed to the mounting panel. The gaps are $^1/_{16}$ inch wide and break down at about 3,500 volts.

The unit is supported by its connectors which are soldered to the bonding cables between wall clamps, as shown in Figure 8. The construction is such that a damaged panel can be replaced without disturbing the bonding cables, and provision is made for testing the unit for airtightness after installation. A considerable number of rather violent discharges has been observed during a long series of switching tests, but no serious damage has resulted. The arc burns are small, and the metal vapor is blown clear, leaving the gap in normal condition, although in many cases the surface of the bakelite panel between the electrodes has been discolored by the arc heat.

Oil Supply

The profiles of these lines fortunately have been such as to permit the use of type-*CC*, or gravity feed, oil reservoirs in all cases. The length of section between stop joints varies from a single span on the relatively steep grades at the north end of the Fairfax line to a maximum of five spans where the line is nearly level. The maximum oil head on any part of the cable is 38.3 feet, which gives a hoop stress in the cable sheath of 122 pounds per square inch. The oil requirement of this cable over an assumed temperature range of 56 degrees centigrade is about six gallons per thousand feet, or $4^1/_4$ gallons per normal span. On adding a small allowance for the joints and the customary 30 per cent spare oil, it was found that the requirements of all of the different sections could be met most economically by installing various combinations of 13- and 19-gallon reservoirs.

In all sections of two or more spans, the oil-supply system for each cable is completely independent, except for the provision for emergency interconnection through the valve panel. In single-span sections, such separation would require the use of a considerable number of special small reservoirs, and the three cables therefore are connected to a common reservoir. The risk of contamination of one length of cable by a failure in another is not appreciably greater than in the usual case of two or more spans connected in series.

Arrangement of Manhole Equipment

Figure 8 is a general view of the equipment in a typical stop-joint manhole. In normal-joint manholes the arrangement is similar, except that the oil-supply system and the joint stiffeners are not required. Cable joints in both compartments of the manhole are installed with the ends containing the sheath insulators nearer to the center wall, to permit standardization of the connections to the bonding transformers, which are mounted in the corners adjacent to this wall as shown in Figure 5. Neutrals of the bonding transformers on the two circuits are connected together and to the signal-cable sheath to distribute fault current over all available paths, and, for reasons of safety, all cable racks and oil reservoirs and a four-inch lead strip laid under the manhole floor, are bonded to this neutral point with a number-8 rubber-insulated wire.

For convenience in tracing connections, a uniform relationship between the bonded parts is maintained throughout the system. Thus, for example, the left end of the top joint casing is always connected to the upper left electrode in the spark-gap unit and to a definite terminal on the left bonding transformer. Where a bonding transformer is omitted at one end of a half-length span, the standard bonding cable arrangement is retained, and the four cables terminating at the unused transformer position are connected together. The bonding cables are number-2 stranded copper, with five-kilovolt varnished-cambric insulation to prevent sparkovers due to transient sheath voltages too low to break down the enclosed gaps. Since this cable is rather stiff, the loop between the joint casing and the first wall attachment is shaped carefully to offer a minimum of restraint to the joint movements.

The stiffeners used on stop joints are rigid steel frames designed to prevent displacement of interior joint parts by cable movement, but the cable clamps at either end are made adjustable to permit compensation for any eccentricity of the cable at the time of assembly.

Oil reservoirs are mounted on the ceiling in the end of the manhole toward the cable section which they supply, with oil-level relays adjacent to the manhole head, and all connections are kept in the same end of the manhole, so that a duplicate oil system can be installed in the opposite end when it is necessary to supply two cable sections from the same manhole. In sections of four or more spans, two reservoirs are required on each phase, and the three extra reservoirs normally are mounted in the end of the manhole opposite the regular group. No difficulty has been experienced as yet in avoiding conflicts between these alternative uses of the second half of the ceiling.

Figure 14. Impregnating bottle and valve panel for treating and filling joints, terminals, and reservoirs

The connection between the cable joint and the oil pipe attached to the wall is made with a semicircular loop of all-metal flexible tubing, one end of which is firmly held by a bracket on the joint-stiffener clamp. The oil-pipe insulator is placed between this bracket and the valve attached to the joint casing. This arrangement allows the joint to move freely without applying forces of appreciable magnitude to any of the connections. A valve is also provided on the high-pressure end of each stop joint, but no permanent connection is made to it. All oil lines are three-eighth-inch copper tubing except at the terminals, where one-inch tubing is used because of the longer runs and exposure to lower temperatures.

As indicated in Figures 5 and 8, the cables are supported on porcelain insulators suspended from hinged arms which allow complete freedom of movement. The arms are set approximately perpendicular to the direction of motion of the cable at the point of support, and pads of *P* and *B* tape on the insulators prevent displacement along the cable. No fireproofing is used on either cables or joints.

The counterbalance unit shown below the terminal in Figure 9 was developed to relieve the terminal and adjacent cable sheath of a portion of the weight of the vertical cable riser without restricting normal movements. The unit consists of a woven-wire cable grip suspended from the ends of two counterweighted lever arms. The size of the counterweights is varied to give the desired uplift at each location. Immediately below the cable grip, a light metal hood is clamped to the cable to keep foreign materials from falling into the duct.

Signal System

The absence of any large induced voltage in the signal cable makes it practical to use a simple closed-circuit d-c annunciator type of oil alarm circuit, one unit of which is shown in Figure 10. This, with a similar unit connected to the negative end of the battery and to the oil relay in manhole 1 south, forms a three-wire group which is duplicated for each oil-supply section of the line. The neutral conductor serving all such groups may be quite small since it normally carries no current.

When an oil-relay contact is opened by either low or high oil, the armature of the corresponding annunciator relay is released, lighting its white indicator lamp *W*, and energizing the red alarm lamp *R* and the alarm at the operator's desk through an auxiliary relay. The operator then moves the two-way lever key at the lighted indicator lamp to the "hold" position, clearing the local circuit for other alarms. When the abnormal field condition has been corrected, the closing of the reservoir contact re-energizes the annunciator relay, extinguishing its indicator light and again sounding the alarm. The operator then restores normal circuit conditions by releasing the "hold" key.

Each unit of the system is tested periodically by moving the lever key to the "test" position. This opens and grounds the field circuit and checks the operation of the relay and its lamp, the station alarm circuit, and particularly the protective arrester, since a fouled arrester would block signals from the field without giving any indication of its abnormal condition. The arresters are of the rare-gas type with a breakdown rating of 200 to 400 volts.

The signal cables used have either 25 or 43 number-19 copper wires insulated with two half-lap layers of four-mil manila paper, and a 40-mil belt insulation. They are impregnated with petrolatum compound in substantially the same manner as solid-type power cable. The belt insulation provides protection from changes in local sheath potential caused by faults or switching, but, if flashover of an oil relay should place this potential on the cable conductors, the station equipment will be protected by the arresters. In such a case, the neutral conductors in the immediate vicinity of the faulty relay must carry the combined currents from a considerable number of relay wires. The resulting current densities are held within safe limits by using a tapered neutral group, which, in the Fairfax line, varies from two wires at the home station to four at the remote end.

One pair of wires in the signal cable is used to control ventilating blowers (described in a later paragraph), and a second is used to measure the average temperature of the conduit line. Idle wires have been used to form similar temperature-measuring loops in several individual spans having different soil characteristics.

Connections between the signal cable and the oil-reservoir relays are made with number-14 duplex cable with oil-resistant synthetic insulation. This cable is looped through a waterproof test outlet to give access to the circuit for periodic checking of the relay settings.

Conduit Ventilation

To reduce further the hazard resulting from seepage of stray gas into the ducts and manholes, a ventilating system has been installed to maintain a slow circulation of air through all ducts. On the Fairfax line, two three-quarter-horsepower blowers are used, and outlet vents are provided at both ends and near the middle of the line, the remainder of the system being sealed. While a large part of the air supplied leaks out along the conduit, the small amount which reaches the outlets is sufficient to scavenge the ducts quite effectively.

Table I. Distribution of Temperature Rise in Degrees Centigrade

Circuits in Use	2		1	
Load Per Circuit, Kva	125,000		165,000	
Kind of Loss Causing	Dielectric	Load	Dielectric	Load
Cable paper	1.6	4.1	1.6	7.2
Duct air	2.9	3.8	2.9	6.5
Conduit	2.8	2.8	2.1	3.8
Soil	14.0	12.9	8.1	12.8
Total	21.3	23.7	14.7	30.3

If a cable failure were followed by an oil fire in a manhole near one of these ventilating blowers, the resulting damage might be increased greatly by continued operation of the blower. To remove this danger, the blowers are controlled by relays energized by the signal-system battery through two of the signal-cable wires. This circuit is so arranged in the station that it will be opened by any automatic operation of the 138-kv line switches, but not by normal switching operations. The blower relays must be reclosed manually after being released.

Electrolysis and Electrolytic Grounding Cells

Ground-potential surveys made shortly after these cables were installed, revealed that the entire sheath system of the Fairfax line was from 50 to 200 millivolts positive to the adjacent soil. Street-railway tracks cross the line at three points, but the sheath potentials could not be traced directly to them, and trial drainage connections at various points produced negligible improvement. Tests with storage batteries and temporary anodes indicated that a system of cathodic protection would correct the situation, however, and selenium rectifiers with steel-rail anodes therefore have been installed at two locations on the Fairfax line and one on the Wilmington line. While tests and adjustments have not been completed, it is believed that between five and ten amperes at each rectifier will give adequate protection throughout the line.

The standard receiving-station ground system consists of a number of deep wells having copper electrodes. These, with the lead cable sheaths in contact with the soil water, form a huge galvanic cell in which the station ground is positive to the cable sheath. Installation of the cathodic protection system increased this difference of potential from about 150 millivolts to an average of 550 millivolts.

A special type of electrolytic cell has been developed to neutralize this voltage while maintaining an adequate fault-current path from cable sheaths to station ground. The cell consists of three lead plates and two heavily silver-plated copper plates, each about six inches square, in a solution of potassium nitrate. It has an open-circuit voltage of about 750 millivolts, and, when it is connected in opposition to a smaller voltage, the circulating current quickly falls to a negligible value, probably because of polarization effects. Opposition voltages greater than the natural cell voltage result in some deterioration, particularly at the silver plate. Such a cell will pass a current of 8,000 amperes for 30 cycles without serious disturbance, with a drop of about 25 volts across the cell.

One of these grounding cells is connected between the cable sheath and the station ground bus at each cable terminal. In Figure 9, the cell may be seen just back of the counterbalance unit. Provision also has been made for the installation of similar but smaller cells between the bonding transformer neutral and the manhole hardware in each manhole, if experience and tests show them to be desirable.

Installation

The cable and accessories for these lines were furnished by two manufacturers, each of whom supplied the cable, with the necessary joints and terminals of his own design, for a complete circuit. All of the oil reservoirs and their accessories were of one make, as were the bonding transformers and the signal cable. The work was done during the period of most acute material shortages due to military preparations, and the resulting delays so disrupted installation plans that any discussion of schedules and progress would be of little value. Brief description of some of the special devices used in the installation may be of interest, however.

All connectors in the joints and terminals are of the solderless type and were pressed on in a 20-ton four-ram hydraulic press. Maintenance of positive oil pressure in the cable during this operation requires that the cable end be raised up close to the manhole ceiling. The problem of handling the heavy press in this position was simplified greatly by suspending it from a removable bar passed through a series of eyebolts in the manhole ceiling directly above the joint position. A lifting eye attached to the top of the press holds the latter at the proper angle to match the raised cable end, as shown in Figure 11, and the cable clamp, attached to the bottom of the press, ensures exact alignment.

In all of the normal joints and in one make of stop joint, it was necessary to fill the press indentations in the connector to exclude air and provide a firm foundation for the insulating tape. A lead plug, developed for this purpose, was cast in a metal mold and was firmly seated in the depression with a simple hand press having jaws to fit the curvature of the connector surface. Similar plugs were made for the recesses at the ends of the connector valves in normal joints, and for connector-bolt recesses in one of the stop joints.

Table II. Induced Sheath Voltages With Typical Cable Arrangements

Cable Arrangement	Volts Per 100,000 Ampere-Feet			
	Two Circuits		One Circuit	
	Max	Min	Max	Min
Hexagonal	3.46	3.46	4.55	3.79
Circular (Figure 6)	3.49	3.37	4.46	3.66
Rectangular	4.28	3.44	4.66	3.65

Installation of the large oil reservoirs on the manhole ceiling was facilitated greatly by the use of the hoisting device shown in Figure 12. The reservoir rests in a cradle consisting of two side channels joined at the ends by light chains. Two small pulley wheels are built into the middle of each channel, and the reservoir is raised by two $^3/_{16}$-inch steel-wire ropes running over these and similar pairs of wheels in two short-coupled blocks which are hooked into eyebolts in the manhole ceiling on either side of the reservoir position.

The "condenser carrier" shown in Figure 13, also proved to be an effective labor-saving device in handling the factory-built condenser unit which weighs about 150 pounds and forms the major part of one of the stop joints used. The condenser is placed on the carrier while the latter is at one end of its track, which in turn rests on the permanent cable-rack arms. It then is rolled to the position shown, at the opposite end of the track, while the prepared cable end slides through the center opening in the condenser, in which the clearance is quite small. The other cable end is then aligned and connected, and the condenser unit is moved back to its final position over the connector. After the casing end bells are assembled, only a slight elevation of the joint is required to permit removal of the carrier and installation of the stiffener frame.

Gas-free oil for treating and filling the joints and reservoirs was supplied by a portable degasser, mounted on a truck with a 15-kw 110/220-volt gasoline-driven single-phase generator, which supplied power for lights and vacuum pumps as well as for the degasser. Hot oil for washing joint parts was obtained from a closed tank, heated through an oil bath by a standard splicer's furnace.

The special impregnating bottle and valve assembly shown in Figure 14 was developed to reduce the time required for setting up the treating equipment and eliminating leaking connections. It provides for all of the usual operations of evacuation, vacuum test, carbon-dioxide flushing, filling, draining, pressure application, and inspection of incoming and overflow oil. The few field connections are made with copper tubing and flared fittings, with the exception of a short length of heavy rubber tubing between valve panel and vacuum pump to block vibration. The unit is designed to sit on the manhole floor to insure rapid drainage of the bottom joint, and all fittings slope toward the bottle to avoid the formation of oil traps. The symmetrical but not too regular arrangement of the valves facilitates prompt recognition of their several functions, which are further identified by the colors of the triangular closed position markers on the valve handles. White indicates vacuum; yellow, carbon dioxide; green, treated oil; and black, drain valves. Rough vacuum and pressure values are indicated on a dial gauge at the top of the panel, while high vacuum readings are obtained with a "thermal-conductivity" vacuum gauge which employs a heated filament in a simple bridge circuit. It is useful over a limited range from about 0.05 to two millimeters of mercury. The enclosed bulb of this device is shown directly below the dial gauge.

Cable Vibration

Shortly after these lines were energized, the cables were observed to be vibrating at many points. This vibration was easily felt with the hand and in some manholes was clearly audible. It was generally stronger in the cable bends but extended well into the ducts in some cases. Definite dead spots also were observed, having no apparent relation to the points of support. Further exploration with a sensitive crystal pickup, used with a wave analyzer and a cathode-ray oscillograph, confirmed these longitudinal variations and also revealed similar differences in intensity and a wide change in phase around the cable circumference. The fundamental frequency was 120 cycles per second.

This vibration obviously must be caused by either electrostatic or electromagnetic forces. The latter possibility was eliminated by observing that there was no perceptible change in the vibration of the cable below a terminal at station *D* when the load on that cable was dropped by opening the station-*D* switch. Tentative conclusions reached are that the vibration is caused by electrostatic forces, probably acting on a few outer strands of the conductor which have been loosened slightly by reason of the fact that the length of the bend is not an exact multiple of the length of strand lay, and that it is comparatively harmless. After several months of operation, the vibration seems to have decreased somewhat, but not to such a degree as to justify any hope that it will disappear entirely.

Appendix. Statistical Data

Fairfax Line

Length, miles per circuit	5.03
Three-phase circuits installed	2
Number of stop-joint manholes	16
Number of normal-joint manholes	25

Wilmington Line

Length, miles per circuit	2.49
Three-phase circuits installed	1
Number of stop-joint manholes	4
Number of normal-joint manholes	16

Transmitting Capacity

Each of two circuits, summer, kilovolt-amperes	125,000
Each of two circuits, winter, kilovolt-amperes	142,000
One circuit, summer, kilovolt-amperes	165,000
One circuit, winter, kilovolt-amperes	183,000

Cable

Conductor area, circular mils	1,500,000
Clear diameter of oilway, inch	0.69
Conductor diameter, inches	1.635
Strands, number and diameter, inch	136/0.1056
Insulation thickness, inch	0.650
Superdense paper, per cent	40
Normal paper, per cent	60
Sheath, copper-bearing lead, inch	0.180
Nominal diameter, inches	3.295
Weight, pounds per foot	17.1
Maximum voltage rating, kilovolts	162
Copper loss at 125,000 kva per circuit, watts per foot	2.46
Dielectric loss at 138 kv, 0.75 per cent power factor, watts per foot	1.92
Charging current, amperes per mile	16

Manholes

Type	Two compartment, tandem
Material	Reinforced concrete
Spacing, straight runs, feet	710
Spacing, corners, feet	350
Compartment, width, length, and depth	
For stop joints, feet	6 by $21^{3}/_{4}$ by 9
For normal joints, feet	6 by $19^{1}/_{2}$ by $7^{1}/_{2}$
Vertical joint spacing, inches	20
Length of transition conduit section at each end of manhole, feet	20

Conduit

Material	Fiber in concrete
Arrangement of power ducts	Circular
Radius of duct circle, inches	$8^{1}/_{2}$
Number of $4^{1}/_{2}$-inch ducts	6
Location of three-inch signal duct	Center
Minimum circuit spacing, inches	9
Phase spacing, inches	7.7
Section, width by height, inches	$27^{1}/_{2}$ by $24^{3}/_{4}$
Minimum depth of cover, inches	48

Oil Reservoirs

Type	*CC*—Gravity feed
Number on Fairfax line, 19 gallons	50
Number on Fairfax line, 13 gallons	44
Number on Wilmington line, 19 gallons	24
Number on Wilmington line, 13 gallons	24

Induced Sheath Voltages

Volts per half span, *A* and *C* phases	
125,000 kva on each circuit	6.8
165,000 kva on one circuit	11.5
Bonding transformers	Type *B*, three-phase, 20.8 volts
Number ot transformers installed	
Fairfax line	152
Wilmington line	37

Cable Movement (One End of Manhole)

Total movement (60-degrees-centigrade range) inches	4.3
Lateral movement of joint, inches	5.5
Daily cycle (ten-degrees-centigrade range) inch	0.2
Radius of expansion bends, inches	42
Angle of expansion bends, degrees	60
Joint offset, inches	53

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Problems in Applying Protectors to Electric Aircraft Motors

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WHAT, theoretically, should be required of a protected aircraft electric motor?

This question is one which has caused much discussion among those engineers who are vitally interested in this subject and about which there still seems to be a great deal of misunderstanding and indecision. Basically, almost everyone would agree that the protector should allow the motor under any condition to carry its load until just the instant before burnout and then, and only then, take the motor off the line.

If the protected motor actually could be built this way, then there would be no need for further discussion, and it would become quite evident to almost everyone that a protector would be desirable, if not a "must," on every aircraft electric motor. Then, why all this indecision and misunderstanding? Is it that protectors cannot be built and applied which can match motor characteristics? Or is it that motors do not have *fixed temperature burnout points?*

In this paper, it is hoped that it can be shown that protectors can be made and applied which do follow motor characteristics very closely under all operating conditions. On the other hand, a motor as a general rule has an operating life based on temperature versus time. This means that there is no fixed temperature up to which motors can be safely operated and will instantaneously burn out if this temperature is exceeded.

If the statements made in the preceding paragraph be accepted for the time being, the major problems of engineering protectors into aircraft electric motors become:

1. Locating and measuring the temperature of the danger hot spots within the motor under all conditions of operation.
2. Establishing the life expectancy of these danger hot spots at different temperatures.
3. Fixing the minimum life expectancy which is acceptable for each particular application.

When the foregoing information is complete, it then becomes a mutual problem between the motor manufacturer and the protector manufacturer to apply a protector to the motor. To do this, they must decide what style of protector to use; establish its location in or on the motor; and adjust the thermal characteristic of the combination of motor and protector so that under any load, under any ambient, and at any altitude, one of the hot-spot temperatures will approach its maximum permissible value before the protector takes the motor from the line.

Locating and Measuring the Danger Hot Spots

In locating and measuring the danger hot-spot temperatures, it should be kept in mind that it is *not necessarily the hottest spots in the motor which may be damaged first.* For instance, a motor might have the clutch coil operating much hotter than the solder in the commutator risers, but the solder would be thrown when it reached its melting point and destroy the motor long before the hotter clutch coil had become more than slightly damaged.

On the other hand, under one set of running conditions, *one element in the motor may be the danger hot spot and the same motor operating under other conditions may have a different element become the danger hot spot.*

Two of many possible examples are cited below:

1. Under locked rotor condition, the brush pigtail may be the thermal limiting element, but, when running at rated load, the pigtails may have plenty of thermal capacity, and the danger hot spot be shifted to such a point as the rotor windings.
2. Under heavy overload conditions, in a high ambient temperature, the series field of a certain motor may be the limiting factor, as far as danger hot-spot temperatures are concerned. But, the same motor in a very cold ambient temperature carrying a heavy overload will have the danger hot spot shifted to the rotor. That is, under this latter condition, the rotor will be running hotter than the series field and, therefore, becomes the limiting factor.

With the thought in mind that the *danger hot spot may not be the hottest spot in the motor*, and that the danger *hot spot may shift* from one element in the motor to another as the operating conditions are varied, it becomes necessary to test the motor to ascertain where these hot spots are under all conditions of operation, and it is also necessary further to measure the actual temperatures encountered.

As a general rule, the following are the most usual points that have to be considered:

1. Rotor winding.
2. Field winding.
3. Clutch or brake coil.
4. Solder on commutator.
5. Brush pigtail.

However, less usual limiting danger hot-spot temperatures appear in some of the following members:

1. Brush spring.
2. Solder in end of brush pigtail.
3. Brushes.
4. Commutators.
5. Bearings.
6. Lead-in wires.

In measuring hot-spot temperatures, it is advisable to determine many of the different temperatures by two separate methods. For example, it is desirable, if not almost necessary, to measure the field-winding temperatures by thermocouples soldered directly to the field windings at one or more points, and also by the resistance-rise method. One of the easiest test procedures for obtaining temperatures by resistance rise is as follows:

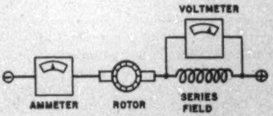

Figure 1. Connections for obtaining temperature of series field winding by resistance rise method

When any test is being run on the motor, an ammeter should be placed in the line and a voltmeter across the series field (Figure 1). By obtaining a simultaneous voltage and current reading when the motor is at room ambient temperature, the resistance of the windings at the known temperature can be calculated by Ohms law ($R=V/I$). When the motor is at the condition where the unknown temperature is desired, another simultaneous reading of voltage and current will give the hot resistance. The formula:

$$R_2/R_1=234.5+t_2/234.5+t_1$$

where

R_2 is resistance at known temperature.
t_2 is known temperature in degrees centigrade.
R_1 is resistance at unknown temperature.
t_1 is unknown temperature in degrees centigrade.

gives the change in resistance of standard annealed copper with change in tem-

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perature. From the foregoing formula the final temperature of the windings can be calculated. However, an easier method than making this calculation is to use a chart similar to Figure 2. When the ratio between the hot and cold resistance is known, the final temperature of the winding can be taken from this chart directly.

If the average temperature of the field windings by resistance rise and the temperature of specific points in the winding by means of thermocouples are known, it is a relatively easy problem to estimate the hot-spot temperatures. Also, by using two entirely separate methods of measurement, the testing engineer is more certain of the accuracy of his results. This is extremely important as we desire to carry these windings up to *extremely high temperatures where life is comparatively short.*

Clutch windings, brake windings, shunt fields, and the like, should be measured with the same care and by a similar method.

Rotor windings under locked-rotor conditions can be obtained in a similar manner. In this case the voltage drop can be taken across several bars of the commutator by soldering leads directly to the commutator bars (Figure 3).

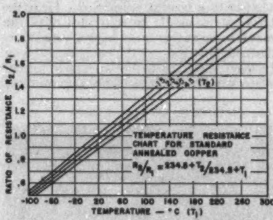

Figure 2. Variation of resistance of standard annealed copper with change in temperature

First, the rotor must be locked absolutely tight so that the commutator will not move with relation to the brushes. Then, two assumptions have to be made:

1. The two halves of the rotor winding will heat alike.
2. The current flowing through the half of the armature to which the voltmeter connection is made is always a certain definite proportion of the total current flowing through the line.

The resistance value thus obtained is not the true resistance of the rotor, but is directly proportional to it, and, since the temperature formulas are based on change in resistance, the final temperature value is accurate. Also, under locked-rotor condition, thermocouples should be soldered directly to the rotor winding for second means of obtaining temperatures (Figure 4). A motor complete and ready to test on locked rotor with thermocouples and voltage-drop leads installed is shown in Figure 5.

When the motor is running, the measurement of the temperature of the rotor requires an entirely new technique. Here, quite obviously, no permanent connection can be made to the motor. Some motor manufacturers actually have drilled out the center of their shafts and let the ends of thermocouple wires from the rotor windings extend to the outside of the motor (Figure 6). Then, on shutdown, a potentiometer can be connected rapidly to these thermocouple ends, and readings of the rotor-winding temperatures can be taken. This is a very successful method, but requires such special handling of the motor as to become somewhat impractical in a great many instances.

Probably the most practical method of obtaining rotor-winding temperatures on running is by the resistance-rise method. This procedure is something as follows: Remove the brushes from the motor. Insert resistance-measuring leads, with sharp points for a good contact, into the brush holders. Then, measure a cold resistance of the armature between two marked bars. Figure 7 shows a rotor resistance being taken on a motor during an actual test, and Figure 8 shows a close-up of two different views of the type of resistance prong used with a double Kelvin bridge. Here the insulated part of the holder is exactly the same size as the brush which it is replacing, and pressure is held on one of the prongs by means of a spring, while the other prong is held in firmly by hand pressure.

After the cold resistance is measured, a test run is made. At the time the rotor-winding temperature is desired, the rotor is stopped and a hot resistance is measured. One of the most successful means of obtaining an accurate shutdown rotor-winding temperature is as follows: At the instant the motor is shut down, a stop watch is started. The brushes are taken from the motor as rapidly as possible, the rotor turned to the two marked bars, the resistance prongs inserted in the brush holder, and a resistance reading is obtained. The time on the stop watch to get the first reading is noted. Then, every ten to fifteen seconds, for about three minutes, both time and resistance readings are recorded. A table of results taken during an actual motor test is shown in Table I. From these data a graph of resistance versus time is plotted. By extrapolating this curve back to zero time, the rotor resistance at shutdown can be obtained accurately. Such a curve is shown in Figure 9. Then, from the chart (Figure 2) the actual temperature can be read. By plotting all such rotor-resistance readings for a given motor on a single sheet of graph paper, each successive curve tends to verify the readings that have been made before.

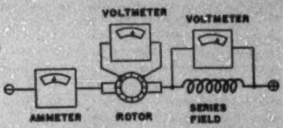

Figure 3. Connections for obtaining temperatures of rotor and series-field windings by resistance-rise method

Figure 10 shows all of the resistance-versus-time curves taken on a particular motor in making a protector application. Note how those curves, where the first reading was obtained after a long time interval, can be used safely, as the other curves on the sheet tend to help establish the shutdown temperature. It also may be noted that the curves taken at cold ambient temperature (−50 degrees centigrade) have the steepest slopes, and those taken at high ambient (+78 degrees centigrade) have the flattest slopes.

It is granted that to obtain such a resistance reading at 38,000 feet and at −65 degrees Fahrenheit is quite a chore It has been done, and the results seem to justify the difficulty of obtaining the data.

A very similar method of measuring rotor resistance which sometimes becomes more handy is to drill a hole in the top of the motor over the commutator and through it measure resistance of two adjacent marked bars (Figure 11).

A close-up of such a resistance probe for use with a double Kelvin bridge is shown in Figure 12. Note that three of the four prongs are held in by spring pressure. This arrangement is highly recommended as it will insure good contact pressure at all four points. It is further recommended that all brushes be lifted for this test and readings always be taken across the same two bars to insure greater accuracy of measurement.

One of the best ways to mark the bars is shown in Figure 13. Here small holes are drilled in the commutator segments. These not only mark the bars so that resistance reading always will be taken across the same two bars, but also serve

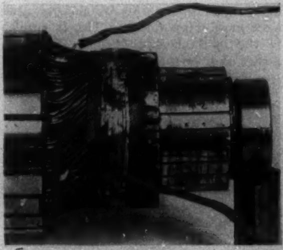

Figure 4. Thermocouples soldered directly to rotor winding

as holes into which the sharp resistance prongs are inserted. This protects the smooth face of the commutator from becoming roughened for further testing. Here again it is felt necessary to plot a curve of resistance versus time to obtain more accurate rotor temperatures.

The temperature of the bars in the commutator and the solder in the risers can be obtained quite easily on locked rotor by means of thermocouples soldered into the respective parts. However, when the motor is running, this presents a more difficult problem. One method which has been used with some success is to paint the parts where the temperature is desired with a temperature paint which changes color when a certain predetermined temperature has been reached. By having several paints with different temperature characteristics, temperatures between limits can be determined.

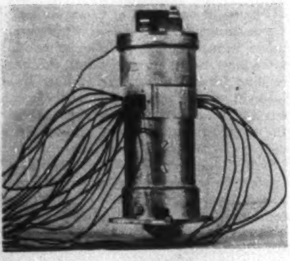

Figure 5. Motor complete with thermocouples and voltage-drop leads for motor protection test

Another method is to use a small rod of bakelite, at the end of which a thermocouple of very small mass has been attached. Then, at shutdown of the motor, the thermocouple is pressed firmly against the parts where the temperature is desired. Since it takes some time to get the thermocouple into the motor and up to temperature, it has been found advisable to plot a curve of temperature versus time and to extrapolate this curve back to zero time to obtain the true maximum temperature. This is similar to the methods employed in Figure 9, except that in this case temperatures are recorded directly instead of resistances.

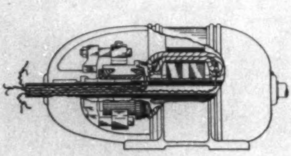

Figure 6. Hollow-shaft method of obtaining rotor temperatures

Pigtail temperatures and lead-wire temperatures can be obtained by embedding thermocouples of very low mass within the pigtail or lead wires themselves. The solder temperature at the end of a pigtail or lead wire can be measured by soldering a thermocouple into the solder itself.

Approximate brush temperatures can be obtained by soldering a thermocouple onto the brush holder at a point as near the contacts of the commutator and rotor as possible (Figure 14), but in those cases where brush temperatures become extremely important it is advisable to drill the brush near the contact end and press in a thermocouple. A brush so arranged, ready to go into a motor, is shown in Figure 15.

Tests That Should Be Run for Preliminary Danger-Spot Investigations

Test 1. Lock the rotor and allow it to remain on the line until it has reached a dangerous temperature. Record the starting amperes, the amperes at the end of the test, and the impressed voltage corresponding to each reading. Obtain rotor-winding temperatures, series-field temperatures, brush temperatures, pigtail temperatures, commutator temperatures, and temperatures of any other part which possibly may become one of the danger hot-spot temperatures. Probably the easiest way to conduct this test is to guess how much time the motor can stand on locked rotor. Then run the above test for not over one-half this time, obtaining and recording the above-mentioned temperatures. Cool the motor back to starting condition. Then repeat the test, allowing the motor to remain on the line a little longer than the original time. After repeating the above type of test for several times, with each locked time longer than the preceding one, a curve can be plotted of the temperatures in different parts of the motor versus locked time. Figure 16 shows such a test being run. Granted, it takes a large number of people to conduct such a test in order to get simultaneous readings, but the accuracy of results and the short time that is required to get the readings will justify the trouble. Figure 17 shows a curve obtained from the data taken as described above.

Test 2. If a motor is of the intermittent-duty type where it cannot carry its duty cycle continuously, start the motor at room temperature at a load equal to its intermittent-duty rating. Operate the motor until it reaches a very high temperature. Record at relatively close time intervals all the temperatures of the different elements within the motor which are at all likely to be the limiting danger hot-spot temperatures. Final rotor-winding temperature similar to the test outlined previously should be obtained at the end of this test. From these data, plot a curve of the motor temperature versus time. Figure 18 is an actual curve obtained in the preliminary stages of applying a protector.

Test 3. If a motor is of the continuous-duty type, operate it at full load, and, when it has reached a stable condition, record all the temperatures of the different elements of the motor. Then increase the load five to ten per cent and repeat the test. Then, in several steps, increase the load. At each one of these load values, the current should be maintained constantly until a stable temperature reading is reached. These increase steps should be continued until the motor is operating at as high a value as is deemed practical. This temperature may be even higher than that to which the designer wants to limit his motor. From these data can be plotted a curve of motor temperature versus motor amperes. Such a curve is shown in Figure 19.

The tests from which these three curves are obtained represent the, minimum amount of testing required to arrive initially at the temperatures to which different parts of the motor can be run and also the minimum test data required to select a reasonably accurate preliminary motor-protection sample for test on the motor. However, before work can be started on a protector sample, it must be ascertained to what temperature the motor hot spots can operate.

Figure 7. Obtaining shutdown rotor resistance after running test

Some of the Outstanding Weak Spots That Have Been Encountered in Different Motors Which Have Been Tested for Protection Application

1. On locked rotor on one motor the brushes glowed red for one-quarter inch from the commutator in less than one second, and high bars appeared in the commutator under the brushes. The rest of the motor was very cool at the end of this one second.

2. On another motor the commutator for about three segments under each brush was a molten mass after two seconds on locked rotor, and it was impossible on the same motor to close and open a 200-ampere knife-blade switch fast enough not to cause damage to the commutator.

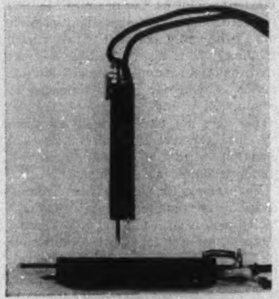

Figure 8. Prongs used to obtain rotor resistance through brush holders

3. On still another motor, when the series-type clutch reached 203 degrees centigrade on an intermittent-duty motor, the series field was 69 degrees centigrade and the rotor was 57 degrees centigrade.

4. The insulating sleeving around the brush pigtails on still another motor would burn off on running long before the rest of the motor was up to a dangerous temperature, and on locked rotor the pigtails would burn out while the rest of the motor remained relatively cool.

5. On a fifth motor the solder would melt on the end of pigtail on running before other parts of the motor were near maximum possible temperature.

6. In a sixth motor the springs holding down the brushes annealed when rotor windings were at a temperature not exceeding 125 degrees centigrade and field windings were 145 degrees centigrade.

7. In still another motor an unbalanced rotor winding was found with certain coils operating very hot, while the rest of the rotor coils ran much cooler.

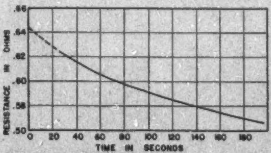

Figure 9. Cooling characteristics of rotor

The foregoing are only a few of the design weaknesses which have shown up in production motors when protectors have been applied. Quite naturally, when such design weaknesses are found in the motor, they should be corrected by the motor manufacturer before the protector is applied, but in a good many cases the manufacturer does not feel that production design can be changed to correct these features. Therefore, in those cases the thermal characteristics of the protector must be adjusted to limit the danger hot spot in the weak member. Where this weakest spot shows up on, say, locked rotor but is not the limiting factor on running at rated load, the adapting of the protector becomes quite a trying problem. *But, even here a properly designed and applied protector* under all conditions should carry the motor up to approximately the point where one of the elements is reaching its danger hot-spot temperature before taking the motor off the line.

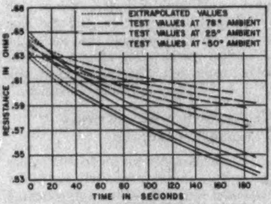

Figure 10. Cooling characteristics of a rotor for various ambients and load conditions

What Is the Life Expectancy of the Different Parts of the Motor?

Windings

As has been pointed out before in this paper, the life of most parts of a motor is based on temperature. To start with, let us consider the life of the windings in the series field, shunt field, clutch, brake, rotor, and so forth. Mr. Scott and Mr. Thompson in their paper on "Temperature-Aging Tests on Class-*A* Insulated Fractional-Horsepower Motor Stators,"[1] make the following statement: "Below 200 degrees centigrade there is no temperature which, if exceeded, causes an abrupt failure of the insulation; that is, the temperature-aging curves of the insulation of these motors are uniform and continuous." In this paper they point out that for every 10 to 11 degrees centigrade increase in winding temperature, the life of class-*A* windings is cut in half. A reproduction of one of their curves of insulation life versus temperature is shown in Figure 20.

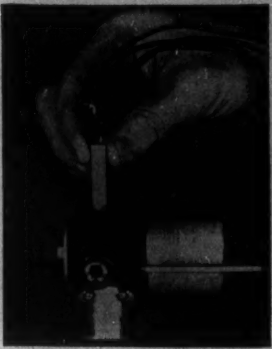

Figure 11. Measuring rotor resistance

Douglas F. Miner, in his book, "Insulation of Electrical Apparatus,"[2] closely verifies the afore-mentioned life value. He finds from an entirely different procedure that the life of class-*A* insulation is halved for each increase of approximately eight degrees centigrade temperature, and for class-*B* the life is halved for every $10^1/_2$ degree centigrade increase in temperature. A reproduction of his curve of life versus temperature for class-*A* insulation is shown in Figure 21.

From the preceding information it can be seen that there is not a cut-and-dried temperature at which a motor may run, and a slightly higher temperature at which it must be taken instantly from the line. What is required is an engineering compromise. That is, it is necessary for the engineers interested in the particular application to specify a minimum life and let the maximum winding temperatures be fixed by this chosen life.

Commutator

If the solder in the commutator riser is the limiting hot spot, this is ascertained easily. Either the solder throws or it does not. This is approximately a fixed temperature, and it is possible to operate for a long period of time fairly close to the throwing point. The easiest way to get this point is to operate the motor

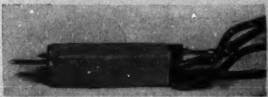

Figure 12. Resistance probe as used with double Kelvin bridge

under the conditions which cause the highest heat concentration in the commutator until the solder is actually thrown.

How high commutator temperatures can rise under locked-rotor conditions before the squeezing action under the brush causes high bars is something difficult to ascertain. One method that has worked out satisfactorily is as follows: When commutator temperatures are known to be running dangerously high on locked-rotor test, the commutator, after such tests, should be measured to locate any possible elevated commutator segment. Should none appear, then several production protected motors should be operated for five hours on locked rotor. These motors should then be taken apart and examined for the high bar difficulty. If no high bars appear in any of these motors, the motor manufacturer can be reasonably sure that the motor will experience no abnormal squeezing of the commutator segments.

Pigtail—Brush Spring

The pigtail in most motors is not covered and can run quite hot if need be. Temperatures in the red-heat zone are not recommended during locked-rotor conditions, but temperature where interference colors are formed is not too objectionable, provided this heat does not affect other members.

For instance, an insulating cover over the pigtails would be one limiting feature. In this case the temperature would have to be held to a value where the insulating members would not be burned. Another instance occurs where the heat from the pigtail heats the brush springs. These in turn will be annealed and lose their pressure, causing bad commutation. No exact temperature figure to which springs can be operated seems to be available, but here again tests conducted under the worst heating conditions for the springs will tell whether the spring in question will be annealed in the period of life expectancy desired.

Brushes

Experience has shown that under running conditions the temperature of the brushes has never been a limiting factor in applying a protector. But, on locked rotor in certain designs the brush temperatures have been a decided limiting factor. Brush contacts with the commutator on locked rotor have been seen to glow red before any other element in the motor had reached a dangerous spot temperature high enough to warrant setting the protector to open. Quite naturally, the possibility of such high temperatures cannot be allowed to exist in a protected motor as such temperatures would cause difficulty, such as high bars, rough commutators, and the like. It must be borne in mind that under the *AN-M*-10 specification, after a five-hour locked-rotor test, the motor must still be in satisfactory operating condition.

Table I. Cooling Data of Rotor

Time (Seconds)	Resistance (Ohms)
25	0.6350
32	0.61975
42	0.61375
53	0.6085
63	0.6040
72	0.6005
84	0.5965
94	0.5935
104	0.5905
117	0.5875
125	0.5840
138	0.5805
150	0.5775
160	0.57475
170	0.5720
182	0.56925

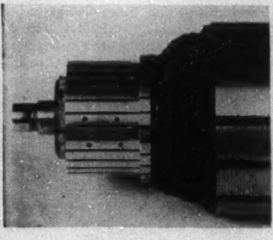

Figure 13. Locating holes in commutator segments for measurement of resistance

Solder on Pigtails and Other Solder Connections

All these connections made with solder can operate almost indefinitely just below the solder-softening point. This softening point value is obtained easily and can be measured from thermocouples directly imbedded in the solder. Therefore, it is quite easy to ascertain whether they are the limiting hot-spot temperatures.

Bearings

Although there has been a great deal of discussion about life of bearings at elevated temperatures, a motor protector set to limit all the other hot spots in the motor to proper values normally will allow the bearings to operate for the minimum life expectancy.

To point out how widely different the anticipated life of bearing versus temperature can be with different greases, Figure 22 shows five different curves on bearing life with different lubricating material. The figure on each curve shows the minimum temperature recommended for each grease. Note the lower the minimum temperature recommended for the grease, the shorter the bearing life at any given temperature. Also note the wide difference in life between two so-called low-temperature greases. These curves should not be used for accurate-design purposes. Each grease by each manufacturer has its own life characteristics, and these curves should be obtained and used instead of the curves shown in Figure 22.

What Is the Minimum Life Expectancy Desired?

To begin with, aircraft-motor specifications today say that a motor must stand five hours locked rotor, and after this it shall be in such good condition that it shall be able to pass all of its other life requirements as set up by the specification.

Beyond this, we have very little written matter to guide us, but in aircraft motors it is necessary to get everything possible out of the motor before it is taken from the line. It is also a well-known fact that the total running life on any aircraft motor is relatively short compared to commercial motors. Therefore, the designer does not have to build as much life into the aircraft motor. This, in effect, means that he can run the aircraft motor at much higher temperatures.

For example, the Underwriters' Laboratories' listing requirements say that protected motors shall not run at a winding temperature over 125 degrees centigrade for class-*A* insulation. Miner's curves, Figure 21, give a *minimum life* expectancy of 400 days, and the Scott and Thompson curve, Figure 20, gives a *minimum life* of approximately 450 days. No aircraft electric motor would be expected to *operate for such a long time under distress conditions*. Other known facts are that most intermittent-duty motors never will operate for more than a few hours during the entire life of an airplane, while some continuous-duty motors may operate every minute the airplane is in the air and can accumulate a considerable number of hours of running time. Therefore, it becomes evident that as a general rule the intermittent-duty motor can be operated to higher temperatures than can the longer-life continuous-duty motor.

Thus, it has been concluded from all data available that the class-*A* windings

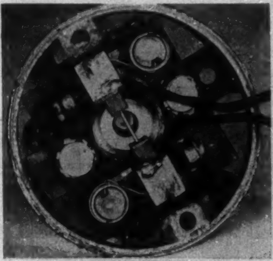

Figure 14. Thermocouples soldered to brush holders

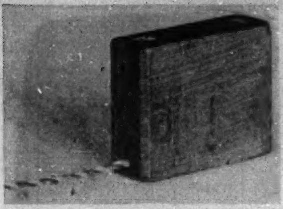

Figure 15. Thermocouple embedded in brush

on highly intermittent-duty motors should be limited by the protector to approximately 200 degrees centigrade, which, from the two temperature versus life charts, Figures 20 and 21, gives a minimum life expectancy of 21 hours to five days. It has been concluded further that continuous-duty motors be limited to approximately a winding temperature of 178 degrees centigrade, which gives a minimum life expectancy of from five to 18 days, as illustrated in Figures 20 and 21.

Quite naturally, the temperatures given are not fixed and absolute. They are starting points from which to study each individual application as it comes up. Should the windings be of a better grade than shown, naturally temperatures should be increased. On the other hand, should it be deemed necessary to get more work out of a particular motor in an emergency than these maximum temperatures will allow, it is possible by sacrificing some of the life expectancy in the motor to set the protector to limit the motor windings to some higher value.

Most of the other danger hot spots are more dramatic in their manifestations of danger than are the windings, and, under those conditions where they are the limiting factor, the temperature at which damage occurs usually can be determined to quite close limits. In most motors, by dropping the maximum temperature allowed by the protector only a few per cent below these known values, the motor will last its required life.

It should be pointed out that under *normal operating conditions the motor should not operate up to these maximum hot-spot temperatures.* The different hot spots will operate at the same temperature at which the motor would operate if it did not have a protector, and the expected life of the motor under normal load is not in any way affected by the protector. *The minimum life cited is only expected if the motor always operates under a distress condition.*

The protector is similar to a safety valve or a high-limit control and never should be called on to operate unless some unusual condition is encountered. It must be remembered that in a high-flying airplane altitudes of 30,000 to 40,000 feet and temperatures of −65 degrees centigrade or lower are not unusual conditions. They are the usual conditions, and motors should be designed to take care of these conditions without operating the motor at a temperature high enough to cause the protector to open. *If the protector does have to open under these conditions, then the motor itself is not the one for the job.* If, on the other hand, the motor does encounter unusual conditions, then after the motor has done all it is thermally capable of doing, *and only then*, should the protector take the motor from the line.

How Protectors Are Applied

Having run the three tests as outlined in section marked "Tests That Must Be Run for Preliminary Hot-Spot Investigation," and having established the minimum life expectancy for the particular motor in question, as outlined in section, "What Is the Life Expectancy of the Different Parts of the Motor," the motor designer is ready to apply a protector to the motor.

First, it is necessary for the motor and protection engineers to ascertain what style of protector is to be used and where it is to be mounted. The protector engineer's first desire is to try to place the protector at a location as closely related thermally to the hot spot as possible. The motor engineer's problem is then to design the protector into his motor, keeping in mind the request of the protection engineer, but also looking out for his own particular problems of manufacturing. It might be pointed out here that regardless of the final protector location a thermal protector usually can be worked out which can protect the motor satisfactorily. But, on the other hand, the further the protector is mounted away from the hot spot thermally, the greater is the engineering job of adapting a protector to the motor.

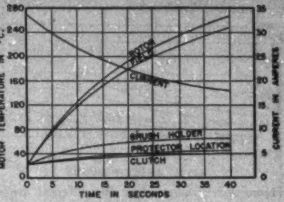

Figure 17. Temperature in different parts of the motor versus time for locked-rotor conditions

With the mounting of the protector decided upon, a protector is selected so that it will meet the one locked-rotor condition already run at room ambient temperature and will also have thermal properties which will meet the conditions indicated in the running tests.

It must be kept in mind that such a protector under running conditions on a certain motor may have such thermal properties as to protect, for example, the large mass of the rotor windings, while on locked rotor the thermal characteristics of the protector must be of such a design that it will protect the small mass of the brush pigtail. And in the example cited, the brush pigtails under this locked-rotor condition may have a thermal mass so small that the rotor may be able to stay on locked rotor five to ten times as long as it would take to burn the pigtails out.

It often has been said, "This can't be done." This would be building two entirely different thermal units with entirely different thermal mass charac-

Figure 16. Testing a motor under locked-rotor conditions

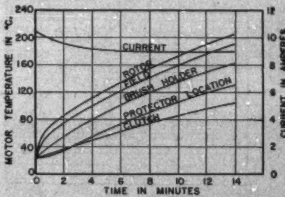

Figure 18. Temperature in different parts of the motor versus time for intermittent-duty load

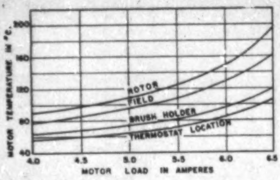

Figure 19. Temperature in different parts of the motor versus continuous load current

teristics into one device. However, in actual practice, a properly designed aircraft-motor protector has to do just that thing. It is being called on to protect, not the windings alone, but the entire motor, and, what is more important, it should allow at least one of the hot spots in the motor to rise to the danger temperature before taking the motor off the line, regardless of the condition under which the motor is operating.

Quite naturally, one thermal-responsive element in a protector in most cases would not take care of such wide variations in thermal mass conditions in the motor. Present-day successful aircraft-motor protectors have at least two thermal-responsive elements, besides taking advantage of the thermal characteristics of the motor itself by virtue of being mounted in thermal contact with the motor. The theory of how this is done has already been presented in a paper entitled "Principles of Aircraft Electric-Motor Protection" by V. G. Vaughan.[3]

As soon as the protector has been mounted on the motor, it then becomes necessary to test and see if the protector will protect not only under the preliminary test conditions, but under all conditions. First, it is desirable to check against the conditions for which the protector was selected. The motor should be locked and connected to a source of supply of proper voltage; complete results of such a test should be obtained, showing time, current, temperature, and so forth. A suggested form and method of recording test data is shown in Figure 23. Figure 24 is a chart taken from a recorder showing the continuous record of the temperature of a thermocouple embedded in the rotor winding for the same test run. This thermocouple is the same thermocouple as shown in column 10 in Figure 23. Such a locked-rotor test should be continued until a steady-state cycling condition is reached. This time is usually less than one hour.

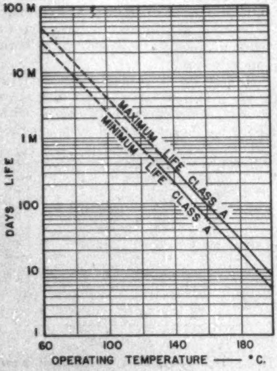

Figure 20. Effect of temperature on life of insulation—Scott and Thompson

After the locked test, another test under the preliminary running condition is made. Here again a complete test is run, obtaining temperatures, times, currents, amperes, and so forth. An actual test report taken from a test run on a motor is shown in Figure 25, and the continuous-recording thermocouple potentiometer chart for the thermocouple embedded in the series-field windings is shown in Figure 26. This is for a continuous-duty motor, and it shows that the motor, at an ambient of 77 degrees centigrade and at sea level, can carry 6.25 amperes continuously with the rotor winding reaching 174 degrees centigrade without opening the protector. However, when the current was raised to 6.50 amperes, the protector tripped, limiting the hot-spot temperature to 176 degrees centigrade.

To connect the motor to the line and let it cycle without any measurements would give a good indication of whether the motor was protected or not. *But it would not tell whether the motor was being operated up to its maximum thermal limits.*

If the first protector will not take care of these two first conditions, then a new protector with different characteristics based on these first two tests must be installed and the foregoing test repeated. Quite often the initial protector will give perfect locked-rotor protection and carry the danger hot spots to the point desired but on the running test the protector will operate before the motor danger hot spot has reached a high enough temperature to warrant taking the motor from the line. Or, in other cases, perfect locked-rotor protection may be obtained, but the motor will not be protected at all on the running test. In each of these cases the protector designer has to change the thermal characteristics of the protector to satisfy the running conditions without affecting it under the locked-rotor condition.

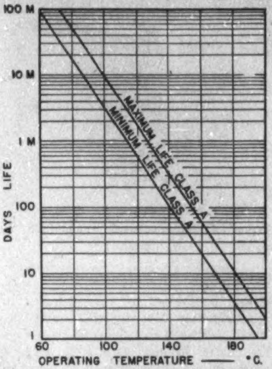

Figure 21. Effect of temperature on life of insulation—Miner

As soon as satisfactory results are obtained on these two tests, it then becomes necessary to check enough of the other operating conditions to make sure the protector will protect under all conditions. As a general rule, when a protector is applied following the theory outlined in Mr. Vaughan's paper, "Principles of Aircraft Electric-Motor Protection," if it will protect under the two conditions previously mentioned, it will be protected under all conditions. However, a locked-rotor test should be run at low ambient temperatures (−54 degrees centigrade) high altitude and at high ambient temperatures (77 degrees centigrade) low altitude. Then loads of several different values should be run under all these temperature and altitude conditions.

On the first motor a manufacturer protects it will require a number of tests to ascertain whether a motor is completely protected, but as he gains experience with one particular design of protector on his line of motors, he can shorten this testing

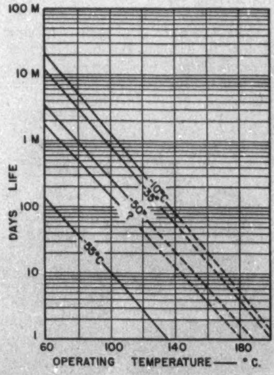

Figure 22. Effect of temperature on life of ball bearings

TEST NO. 1C
MFG. [redacted] MOTOR NO. [redacted] RATING 72W. THERMOSTAT NO. MKA-1
TYPE OF TEST LOCKED ROT VOLTAGE 28.0 AMBIENT TEMP. 29 °C.

TOTAL TIME	TIME ON	TIME OFF	AMPS. ON	AMPS. OFF	VOLTS	RISER UNDER BRUSH	BRUSH HOLDER	SERIES FIELD	ROTOR	CLUTCH	THERMO-STAT LOCATION
	SECS.	SECS.				°C	°C	°C	°C	°C	°C
1CYC.	27.1	16.2	19.1	14.5	28.1	166		166	130	117	
2CYC.	13.3	24.0	15.3	14.3	28.1	175		172	141	131	
3CYC.	12.7	29.0	15.0	14.0	28.1	177		177	158	143	
4CYC.	11.0	35.3	15.0	13.9	28.1	177		172	164	145	
5CYC.	10.1	42.7	15.1	14.0	28.0	177	107	171	169	146	71
6MIN.	10.0	63.0	15.1	14.0	28.0	178	115	165	175	144	85
90MIN.	10.0	86.6	15.0	14.0	27.9	179	121	165	180	141	95

Figure 23. Sample locked-rotor test data sheet

time a good deal. The reason for this is that he soon learns those tests on his own design of motor which are critical and tell him whether the motor is protected or not. But, using the shortest test procedure which will insure perfect protection, it still is a rather lengthy engineering job to apply a protector.

The reason is quite plain. To protect a motor is easy, but to protect a motor under every condition and get the maximum possible output practical is another story. This requires a great deal of engineering thought and testing, which is exactly what every engineer finds when *he must cut his factor of safety in any design down to almost zero*, and that, in effect, is exactly what is being attempted in protecting aircraft motors.

On the other hand, the engineer can draw some comfort for his great amount of work from the fact that as soon as this engineering is complete it is no more trouble in production to put on a protector than it is to put in a brush, an end bell, or the like.

Test Results Obtained on a Particular Motor

To try to include in this paper a complete set of test reports and graphs on even one motor would increase its length unduly. Therefore, for the sake of brevity, only one graph, Figure 27, will be shown. This graph shows the maximum amount of current load the motor will carry indefinitely without tripping the protector at any temperature between −60 degrees centigrade and 80 degrees centigrade at sea level. This is commonly known as the ultimate trip versus ambient temperature curve. Also, on this curve is plotted the steady-state temperatures reached by the different part of the motor under each ambient temperature condition.

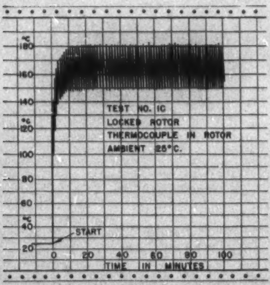

Figure 24. Continuous record of the rotor winding temperature by thermocouple for locked-rotor condition

Note in these curves that the danger hot spot, which was the rotor winding, *varied only two degrees centigrade in ambient temperatures that varied over a range of 140 degrees centigrade.* It also should be noted that the protector allowed the motor to carry almost two times as much load at −60 degrees centigrade as it did at 80 degrees centigrade, but, whenever the rotor winding exceeded 175 degrees centigrade, the protector would open. These curves also show that other elements in the motor may vary widely in temperature with ambient temperature, but the danger spot, for which the protector is adjusted, always is limited to almost a fixed value.

Further testing on this and other motors has shown that the same kind of protection for the danger hot spot can be obtained if the altitude is varied from sea level to 50,000 feet; if the heat-absorbing mass on which the motor is mounted is changed over wide limits; if the load is varied up to and including locked rotor; or if any combination of temperature, altitude, heat absorption, or load is applied.

What Is Required in the Way of Production Tests by the Motor Manufacturer

Several motor manufacturers have suggested the following procedure: the motor be tested, the protector be tested separately, the protector be mounted on the motor, and the protected motor be shipped without further tests. This is not a recommended procedure. The device to be shipped is a protected motor, and at least some tests should be made to prove the performance of the combination as such.

TEST NO. 4A
MFG. [redacted] MOTOR NO. [redacted] RATING 1/10 HP. THERMOSTAT NO. KA-17
TYPE OF TEST ULTIMATE TRIP VOLTAGE 27.9 AMBIENT TEMP. 77 °C.

TOTAL TIME	AMPS.	VOLTS	AMBIENT	BRUSH HOLDER	SHUNT FIELD	FRAME	THERMO-STAT LOCATION	ROTOR	
MIN.			°C	°C	°C	°C	°C	°C	
0	6.0	27.9	77	77	77	77	77	77	
54	6.0	27.9	77	157	142	119	128		
75	6.0	27.9	77	156	142	119	128		
	RAISED TO 6.25 AMPS. AT 76 MINUTES								
107	6.25	27.9	77	159	145	121	130		
120	6.25	27.9	77	159	145	121	130		
	RAISED TO 6.2 AMPS. AT 121 MINUTES.								
129	6.5	27.9	77	161	147	122	131	176	
	PROTECTOR OPENED AT 129 MINUTES.								

Figure 25. Sample running overload test data sheet

Probably the best procedure is to stall the protected motor on the line. The voltage of the source should be held closely at a fixed value, and the time for opening of the protector should be obtained. This time of opening should be between two time limits set up by the engineering department. A great deal of care should be used in setting this maximum and minimum locked-rotor time, as this time is dependent on the variations in the protector, the variations in the motor, the variations in ambient temperature, and the variations in the line voltage. In effect, this test indicates whether the protector manufacturer has furnished the proper rating protector, that it is installed properly, and that there is no major defect in the motor itself.

To give an idea of how rough a test

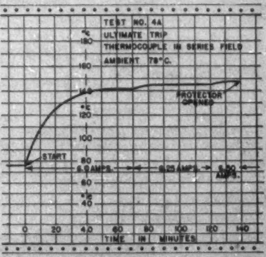

Figure 26. Continuous record of the series-field temperature by thermocouple for ultimate trip test

this appears to be, the following examples might be cited:

One motor manufacturer's time limit on locked rotor is ten to 25 seconds. Another manufacturer has a limit of 50 to 110 seconds. Either of these appears to be quite a wide variation in time and would probably mean to the average motor engineer that the ultimate temperature reached by the motor would be quite different between the motors which had these two extremely wide locked-rotor times.

The engineer who set up the 50-to-110-second time limits took a motor that had a 53-second locked time and one with 108-second locked time. He ran these two motors on an intermittent-duty load until each protector opened. The time the protector took to operate the first time was in the neighborhood of five minutes, and the final hot-spot temperature of the two motors varied only two degrees centigrade. This proved to his satisfaction that wide locked-rotor time did not indicate wide difference in ultimate temperature reached by the motor.

Several motor manufacturers check protected motors in production with a locked test only, and field results have been excellent, tending to prove that this method is quite satisfactory.

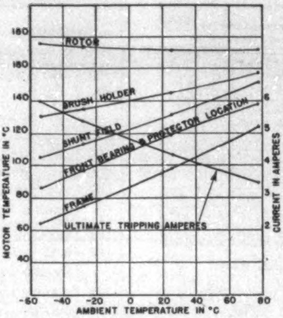

Figure 27. Ultimate trip versus ambient temperature

Another manufacturer tests each aircraft motor for three to five cycles on locked rotor. He obtains the first "on" time and observes the results on the other three to five cycles. Then, every tenth motor, which is of the intermittent-duty type, is put on its intermittent-duty-rated load and operated until the protector opens. This opening time must come between two certain fixed time limits. The danger hot spot of this particular line of motors, under this running load, is the throwing of solder from the commutator risers. So, motors that are run are inspected carefully for solder throwing. However, this manufacturer has had no more rejected protectors from his line than have those that are tested on locked rotor only. In fact, his experience on rejected protectors has been lower than most of those manufacturers who test on locked rotor only. While there is no question that this latter manufacturer is doing more elaborate testing on his production line, it would appear that probably it is not necessary.

Conclusions

As a practical proposition, most motors can be protected up to a predetermined hot-spot temperature even if under different operating conditions the danger hot spot may shift from one element to another, and, if properly applied, this protection is obtained under all conditions of altitude, ambient temperature, and load. The protector is nothing but an electrical-thermal integrating device which tells exactly by the current passing through it and by the heat conducted to it what the different danger hot spots are doing, and, when any one of them gets up to its maximum temperature allowable, the protector opens the circuit. The protector turns it back on when it has cooled somewhat, but stands ready to open the circuit again whenever any of the hot spots again *exceed its own maximum safe value.*

Reams of literature could be written on the subject, and curves by the hundreds could be drawn and presented, but, until an engineer has actually experienced going through the complete test, and then has had such tests verified by putting motors into actual production, it is almost impossible to realize how completely and uncannily this simple compact device always can come up with the right answers. However, the protector does not do it by itself. Many long hours of engineering go with each of these applications, and a device less perfectly engineered may give no end of trouble and grief should it find its way eventually onto an airplane.

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An Analysis to Determine the Optimum Bussing Arrangements and Transmission Capabilities at Grand Coulee

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As a result of the war, the installation of generators at the Grand Coulee generating station, which is located on the Columbia River in the northeastern portion of the State of Washington, has been accelerated to such an extent that the station now generates more hydroelectric energy than any other single station in the world. The station was built and is operated by the United States Bureau of Reclamation. Power from it, as well as power from the Bonneville Dam, is delivered through the transmission system of the Bonneville Power Administration to three major load centers, the Puget Sound area of western Washington, the Spokane area of eastern Washington, and the Portland area of western Oregon. Power is supplied to large electrochemical industries in each load area, as well as through high-capacity ties to the public utilities of the respective areas. The high-capacity ties create a large closely integrated system operating in synchronism in the States of Washington, Oregon, and in Northern Idaho. The combined peak load of these systems exceeds 2,400,000 kw. In the eastern section of this area there are other ties to Montana, Utah, and southern Idaho utilities. Peak load of all interconnected power systems in these five states is approximately 3,000,000 kw. However, this paper is concerned mainly with the areas in Washington, Oregon, and northern Idaho.

The Grand Coulee generating station, together with the transmission lines radiating from it to the east, west, and south, may be considered as the hub of these systems. Six 108,000-kva and two 75,000-kva generators are installed, but, with allowable overloads up to 120 per cent of rated capacity, a total output of 920,000 kw is obtainable. The station was designed so that each generator and transformer operates as a unit without a low-voltage bus or bus reactors, and all switching therefore must be performed on the high-voltage busses as indicated in Figure 1, which shows a simplified schematic diagram of the station.

The critical-material shortage produced by the war has restricted the building of transmission lines and substations in the area to the very minimum. This has resulted in such heavy loads on transmission lines that transient-stability limits often may be exceeded unless special design and operating precautions are taken. All of the power transmitted from Grand Coulee and the Bonneville generating station, the latter having a peak capacity of 560,000 kw, is utilized either for vital war industries, for essential civilian activities, or to displace oil-fired steam generation. For these reasons a high degree of reliability of service is essential, despite the heavy transmission-line loadings.

This paper presents the results of studies which were made of the northwestern interconnected systems, in an effort to obtain a bussing arrangement at Grand Coulee on the high-voltage bus, which would give optimum operating conditions and continuity of service. It is believed that the design and operating factors involved and the bussing arrangement finally selected will be of interest to a large number of electrical engineers.

Statement of Problem

The transmission grid of the Bonneville Power Administration as it will be at the end of 1944 is represented in Figure 5. For simplicity no circuit breakers have been shown in this diagram. Radiating from Grand Coulee are six 230-kv and, two 115-kv transmission lines. The shortest line to Spokane is 82 miles long—the longest line to Portland (J. D. Ross substation) is 270 miles long—while the longest line section without an intermediate switching station, a Coulee–Covington line, is 183 miles in length. These lines must carry the station output, 920,000 kw, averaging 140,000 kw per 230-kv line. However, because of load diversity, local hydroelectric generation, and a diversity in the rainfall in each of the three major load areas, a normal line loading of 160,000 to 180,000 kw often will occur.

In Figure 5, only the BPA transmission system is shown. For simplicity no effort has been made to show the systems of the other utilities supplying Seattle, Tacoma, Spokane, Portland, and other cities in the area. However, since Grand Coulee alone will supply nearly 30 per cent of the total peak load of the area in 1944, it becomes evident that the method used to connect the Coulee generators and transmission lines is a critical factor in operation of all the northwestern interconnected systems. This is especially true during and following system disturbances which may cause instability and a temporary shutdown of vital war loads until generating plants can be resynchronized with each other.

To bus the generators and transmission lines satisfactorily at Grand Coulee, it was necessary to:

1. Obtain a reasonable 230-kv breaker interrupting duty.

2. Match the transmission capabilities of the lines connected to a bus section to the capabilities of the generators connected, for both normal and emergency operation, so that maximum energy can be obtained from all generators in the station.

3. Obtain a bussing arrangement which will give a high degree of transient stability con-

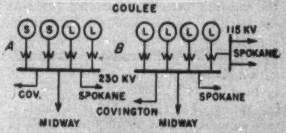

Figure 1. Description of steady-state conditions, cases 1 through 7, with all 230-kv lines in service

(*a*). Coulee 230-kv bus in two sections
(*b*). Two Coulee–Covington lines
(*c*). All 230-kv lines interleaved
(*d*). Eight Coulee generators
(*e*). 427,000 kw of Coulee power delivered to coastal area at 230 kv: 266,000 kw at Covington, 161,000 kw at North Bonneville

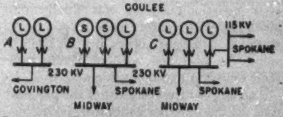

Figure 2. Description of emergency condition, cases 8 through 14, with one Coulee–Covington line out of service

(*a*). Coulee 230-kv bus split into three sections; lines interleaved
(*b*). Eight Coulee generators
(*c*). One Coulee–Covington line—isolated
(*d*). 411,000 kw of Coulee power delivered to coastal area at 230 kv: 214,000 kw at Covington, 197,000 kw at North Bonneville

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The authors thank the Bureau of Reclamation for their co-operation and help in obtaining data for this paper and for their efforts in obtaining and installing the equipment required to make the new bussing arrangements possible.

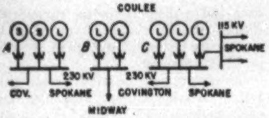

Figure 3. **Description of emergency conditions, cases 15 through 20, with one Coulee–Midway line out of service**

(*a*). Coulee 230-kv bus split into three sections; lines interleaved
(*b*). Eight Coulee generators
(*c*). One Coulee–Midway line—isolated
(*d*). 450,000 kw of Coulee power delivered to coastal area at 230 kv: 312,000 kw at Covington, 138,000 kw at North Bonneville

sistent with the large line loadings normally obtained.

4. Avoid complex switching operations when load shifts from one area to another, or when a change is being made from normal operation to emergency operation when a transmission line or some piece of equipment is out of service.

Solution to the Problem

It was found that with eight generators and all lines connected to one bus section the maximum asymmetrical fault kilovolt-amperes would exceed 5,000,000. The existing station represents approximately half of the ultimate development, and as the existing three-cycle circuit breakers in the lines had a 2,500,000-kva interrupting rating, some form of bus split was immediately indicated. An investigation showed that these breakers could be modified to a 3,500,000-kva interrupting rating at a reasonable cost, and so this value was used as an upper limit in subsequent designs.

In many cases, when the 13.8-kv bus of a large generating station is split into sections to decrease breaker duty, bus reactors are used to obtain synchronizing power between bus sections. When 230-kv bus reactors are considered, however, the high cost, large size, amount of material involved, and the critical physical limitations of the Grand Coulee switchyard made the use of reactors appear questionable. It was finally decided that bus reactors were not required, since a sufficient operating flexibility would be obtained without them, and their omission saved considerable money.

The large output of most of the generators, 125,000 kw per generator, including overload capability, combined with the heavy line loads of 160,000 to 180,000 kw, definitely limited the combinations of generators and lines which could be considered for possible bussing arrangements. Table I gives a comparison of the average line loading obtained for different assumed numbers of lines and generators connected to a bus section, also the total three-phase asymmetrical megavolt-amperes which would have to be interrupted by a three-cycle breaker, assuming that the breaker contacts parted within two cycles following the initiation of the fault. It will be observed in Table I that some line and generator combinations give an average line loading which is quite small; others produce a loading which is very large. When the loading is too small, more lines are required to get the power out of the station, the transmission cost becomes higher, and more critical materials are used for additional lines. When the load is too high, system operation becomes critical, and stability will be lost for a large number of system disturbances.

From a study of Table I and from the results of a large number of transient-stability studies made on the BPA a-c calculating board, it was determined that the combination of four generators and three 230-kv lines per bus section was a practical solution. This was especially true, since it divided the station into two bus sections having very nearly equal generator- and transmission-line capacities. For this combination the interrupting duty on the circuit breakers is low enough to permit the addition of a third similar bus section at Coulee, future lines to the load centers from this bus, and the installation of an intermediate switching station in the long Coulee–Covington lines. This is important as the eight generators now installed ultimately will be increased to 15.

The bussing arrangement finally decided upon is such that the station will be operated in two isolated bus sections at Coulee with a 230-kv transmission line from each bus section to each of the three major load areas or intermediate switching stations, thus forming an interleaved transmission system as indicated in Figure 1 and Figure 5. Synchronizing power between the 230-kv bus sections is obtained through the interleaved transmission lines, although sufficient reactance is introduced by the lines to lower the breaker duty from 5,000,000 kva if solidly bussed, to below 3,500,000 kva, and consequently reduces the deleterious effects of faults on the system. When one line from a bus section to a load center has to be removed from service because of a fault, sufficient synchronizing power is available through the remaining two lines to the other two load centers.

The bussing arrangement is symmetrical in that both lines to each load center normally will be loaded to almost identical amounts, and the phase angle between the two Coulee bus sections will be almost identical. The arrangement has another very important advantage, namely, that the power required in one load area may increase and in another may decrease in accordance with the immediate hydroelectric capabilities of the respective areas, and maximum output from all generators at Coulee still can be obtained without shifting generators from one bus section to another, or without other switching at Grand Coulee. This is an important condition, for past operating experience has shown that the station will operate for weeks at a time at maximum generator output, except for a small reduction in load during a few hours on Sundays. During the long periods of maximum generator loading, the power taken by respective load centers will vary considerably, thus increasing all line loads to one load center while decreasing them to another.

The afore-mentioned bussing scheme also is advantageous in that very little switching is required at Coulee to obtain the emergency operating arrangements shown in Figures 2, 3, and 4. These arrangements would be used in case a 230-kv line to any load area is to be out of service for any considerable length of time. They are necessary to maintain the supply of energy to vital war loads in the area, some of which require a high degree of reliable service.

The emergency operating arrangements of the station were determined on the basis that there is a bad energy deficit in the respective load area; hence the remaining line must be loaded considerably above the normal value. For example, in Figure 1, the normal load of each Covington line may be 165,000 kw, while in Figure 2, the emergency load through the one remaining Covington line may be raised to 230,000 kw. It is expected that spinning reserve will be operating on the systems in the deficient area in the event that a fault occurs on the remaining line to the area. If this should occur, the reserve will make up the generation lost, for when the faulted line is cleared the two Coulee generators would be removed automatically with the line. However, there is ample synchronizing power through the ties to the remaining Coulee busses so that system stability is maintained.

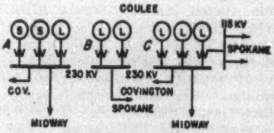

Figure 4. **Description of emergency condition, cases 21 through 28, with one Coulee–Spokane 230-kv line out of service**

(*a*). Coulee 230-kv bus split into three sections; lines interleaved
(*b*). Eight Coulee generators
(*c*). One Coulee–Spokane 230-kv line—isolated
(*d*). 451,000 of Coulee power delivered to coastal area at 230 kv: 275,000 kw at Covington, 176,000 kw at North Bonneville

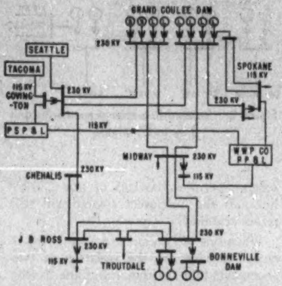

Figure 5. Simplified diagram of BPA transmission grid showing interleaved lines and two Grand Coulee busses

When one of the emergency bussing arrangements indicated in Figures 2, 3, and 4 is in operation and a fault occurs on one of the two lines connected to a bus other than the isolated emergency bus, in order to maintain stability, not only must the line be removed but one of the generators which supplies the faulted line also must be tripped off as rapidly as possible. This requires that the protective relays of the line initiate the tripping of both the line breaker and generator breaker. The latter is an eight-cycle breaker, whereas three-cycle breakers are used in all 230-kv lines. High-speed carrier-current relays are used on the 230-kv lines, which with three-cycle breakers produce an over-all clearing time at both ends of the line of approximately 5.75 cycles. While these relays also will trip the generator breaker, because of slower breaker mechanisms, the total clearing time is about 11.5 cycles.

During such emergency operation it will be observed that, if a generator is not tripped off, the remaining single line to the bus section would have to transmit 285,000 kw, which, of course, is not possible for the longer lines. The tripping of one generator does not seriously disturb the system, as it has been shown that two generators could be tripped without loss of stability, if the isolated line had to be removed.

During normal operation when the bussing arrangement of Figure 1 is in use, if a fault occurs which subsequently leaves two lines connected to bus *A*, it is possible for each of these two lines to carry an average of 210,000 kw without trouble, so that for normal operation no generator has to be tripped.

It is of major importance to observe that, during emergency operation, when the transmission capacity to a certain load area is less than normal, considerably more power can be transmitted without loss of system stability by arranging to trip off one generator, or two generators as necessary, as rapidly as possible upon the occurrence of a fault. In several cases studied, where the system was unstable when only one generator was dropped, its stability was improved when two generators were dropped, but, of course, more spinning reserve must be carried under such circumstances. These results are obtained for the following reasons:

1. During the period of the fault the Coulee generators are partially unloaded and therefore acquire an increased velocity and swing forward in phase position as compared to other synchronous machines on the system.

2. The removal of the heavily loaded line decreases the transmission capability to such an extent that, for a short readjustment period following the removal, the voltage in the load area is below normal, and consequently the load in the area has materially decreased.

3. The removal of a portion of the Coulee generation therefore would tend to balance the decrease in load, and the removal of the rest of the generation produces automatic decelerating power at Coulee which immediately begins to correct for the advance in phase position obtained during the period of the fault.

4. As voltage regulators restore the voltage throughout the system to normal, a readjustment of generator governors also occurs which corrects for the reduced generation. System frequency during the fault first becomes slightly greater than normal, but, after the generators are removed, it drops slightly below normal, and then is restored to normal by governor action concurrently with the restoration of normal system voltage by the voltage regulators.

Discussion of Results

To provide for interleaved breaker operation, some changes in the 230-kv switchyard at Grand Coulee had to be made by the Bureau of Reclamation. These changes involved the moving of a line position and associated equipment, converting all line circuit breakers to 3,500,000 interrupting capacity, together with the purchase of two new 230-kv oil circuit breakers. Not only does this make it possible to use the interleaved bussing arrangement, but also to shift generators and lines for the optimum conditions when a line is out of service.

Results obtained from the stability studies on the bussing arrangements of Figures 1, 2, 3, and 4 are shown in the transient-stability summary (Table II). The table compares the relative stability of the different bussing arrangements for faults located at various points in the system, and each of the cases is summarized. The system prior to the fault is described under "Description of Steady-State Conditions" and by a diagram of the assumed bussing arrangement at Grand Coulee. The type of disturbance is given, namely, either a two-phase-to-ground fault or a three-phase fault. All faults were assumed to be near a substation bus. Their location is indicated by the italicized name of the faulted end of the transmission line under "System Disturbances." The breaker opening time is given under "Clearing Time" and includes the number of cycles necessary to interrupt the current completely, measured from the instant the fault was initiated. On all 230-kv lines carrier-current relaying and three-cycle breakers were assumed, so that both ends of the faulted line were cleared in 5.75 cycles. Faults on 115-kv lines require longer clearing times as shown in the table, because of the use of eight-cycle circuit breakers and sequential relaying. The table also indicates if and when a generator was dropped from a bus section.

For comparative purposes, two phase angles are listed in the tabulation under "Operating Angles." The initial angle is measured between the internal voltages of the two equivalent generators on the calculating board which are farthest apart in phase angle for the steady-state conditions. The maximum angle is measured between the two internal voltages of the equivalent generators whose internal voltages are farthest apart during the disturbance. Should any machine or any group of machines go out of step with the rest of the system, "Unstable" is written in the space provided for the maximum angle.

Transient-stability studies, cases 1 to 5, indicate that for "normal interleaved" operation the loss of any Coulee 230-kv line will not result in system instability, although each Coulee–Midway line is carrying 165,000 kw and each Coulee–Covington line 135,000 kw.

Cases 6 and 7 show faults on the 115-kv system applied for longer periods due to sequential relaying and eight-cycle circuit breakers, and no tendency toward instability is indicated.

For the emergency bussing arrangements not more than two 230-kv lines connect to any one bus section, so that loss of one of these lines will result in instability in most instances, as cases 10 and 17 indicate. The obvious solution was to drop a generator as rapidly as possible from the bus section on which the line is lost. Cases 8, 9, 11, 15, 16, 18, 21, 22, and 24 show the effects of the high-speed tripping of a generator at Coulee. In each of these cases, the system remained stable at the expense of losing 125,000 kw of generator capacity; whereas general system instability would have resulted in each case had a generator not been

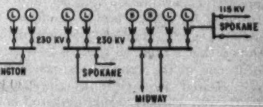

Figure 6. Parallel-line arrangement with one line to Covington

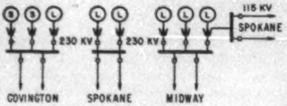

Figure 7. Parallel-line arrangement with two lines to Covington

dropped. The emergency arrangements at Coulee keep the system stable for 115-kv faults near 230-kv receiving-end busses, this being shown by cases 14, 20, 27, and 28.

Before the building of the second Coulee–Covington line, Grand Coulee operated for a long period with a parallel line arrangement similar to that shown in Figure 6, with two generators and two Spokane lines on the center bus, four generators (two large and two small), the two Midway lines, and the Spokane 115-kv lines on the right bus, and two generators isolated on a single Covington line. This system, operating without the second Coulee–Covington line, might represent an "emergency" condition with respect to a parallel-line arrangement as shown in Figure 7. If we refer to Figure 6, a three-phase fault on the 115-kv system near Covington, which was not cleared for 14 cycles, caused the two generators on the isolated line to lose synchronism. The line was carrying 230,000 kw when this occurred. A study was made on the a-c calculating board and the results agreed with system operation, namely, that the fault would shake off these two generators.

Other studies with the second Coulee–Covington line in operation, as in Figure 7, indicated that one generator had to be dropped to maintain stability in the event of a fault on one of the parallel lines. The interleaved connection for "normal" operation does not require the tripping of a generator if a fault occurs on any line. The paralleled-line arrangement of Figure 7 also lacks flexibility when load shifts in the various areas tend to increase the load on one bus and decrease it on another.

With only one Coulee–Covington line in operation, it was believed for a time that the isolated arrangement of Figure 8, for the Covington line and one of the Midway lines, would give very good results. These isolated lines could be heavily loaded, and a fault on either the Covington line or the Midway line would remove two generators from the system automatically when the substation breaker opened in 5.75 cycles, and the stability for 230-kv faults was found to be very good. However, it was determined that a fault on the 115-kv system near one of the substations, which was not cleared so rapidly as the 230-kv faults were cleared, would shake off both generators and the isolated line connected to the substation. Similarly with two Covington lines each isolated at Coulee and with two generators per line, a fault on the 115-kv system near Covington caused both lines to be shaken off, and automatically four generators were dropped; hence this scheme also is inferior to the interleaved method of operation.

Some studies involving the use on the system of high-speed reclosing breakers were made. Reclosing, of course, is effective only if the fault is not reapplied when the breaker is reclosed; however, for the few times that the fault is reapplied, loss of stability is rather certain. This, combined with the fact that considerable critical material and labor would be involved to obtain high-speed reclosing, did not seem to justify its installation at this time.

The effect of installing series capacitors to improve stability was not studied specifically. However, it is believed that after the war there will be many places where such installations will be economically justified. One major factor is that these capacitors materially increase circuit-breaker duty as well as line loading; hence certain major changes in system design are indicated.

Conclusions

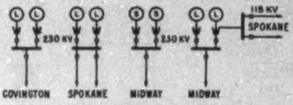

Figure 8. Isolated-line arrangement with one line to Covington

The four bussing arrangements indicated in Figures 1 through 4 should insure satisfactory operation of the Coulee generators and the BPA system. Despite heavy line loadings, the interleaved scheme permits the loss of any 230-kv line and the northwest system will still retain stability.

When one of two lines to a load area is out of service, by the isolation of two generators and the remaining line to this load center plus an arrangement to trip a generator from one of the other busses in case a line outage to another load center occurs, stability can be maintained with only a small decrease in the power to the load centers.

An important feature of the interleaved bussing arrangements is the ease with which a change-over can be made from the normal interleaved to an emergency interleaved scheme upon the loss of a 230-kv line, as no complex switching operations are necessary.

In any high-voltage transmission system where it has been difficult to install additional transmission lines because of critical wartime shortages, the prevention of general system instability through the use of high-speed tripping of generators should be investigated carefully. Contrary to general belief it is not neces-

Table IA. Comparison of Average Normal Line Load in Megawatts, and Maximum Asymmetrical Rms Megavolt-Ampere Breaker Duty for Specified Line and Generator Arrangement on Bus Section

Three-Phase Short Circuit Near 230-Kv Bus—Two-Cycle Total Decrement—Back Feed From Faulted Line not Included

Number of 230-kv lines to bus	2	3	2	3	2	3
Number and size of machines on bus	$3L$	$3L$	$2L+2S$	$2L+2S$	$4L$	$4L$
Power output of machines (megawatts)	375	375	420	420	500	500
Power transmitted per line (megawatts)	188	125	210	140	250	167
Short circuit supplied from machines (megavolt-amperes)	1,680	1,680	1,850	1,850	2,240	2,240
* Back feed from lines	500	975	500	975	500	975
Total breaker duty (megavolt-amperes)	2,180	2,655	2,350	2,825	2,740	3,215

* Back feed is from one Spokane line if total lines per bus are two, and is from one Spokane and one Midway line if total lines per bus are three.

Table IB. Maximum Asymmetrical Rms Megavolt-Amperes Supplied From Each Source

Three-Phase Short Circuit Near 230-Kv Bus—Two-Cycle Total Decrement

From Back-Feeding Lines		From Generators			
			Machine Capacity		
		Type	Rated	Maximum	Short-Circuit on 230-Kv Bus
Spokane line	500	L	108	125	560
Midway line	475	S	75	85	365
Covington line	325				

It should be mentioned that single-phase-to-ground faults near Coulee could produce fault currents 20 per cent higher than three-phase faults if all the transformer-bank neutrals per section were grounded. However, these currents can be reduced to or below the value of a three-phase fault if only one transformer bank is grounded per section. Provisions for such ungrounding are being made.

Table II. Transient-Stability Analysis of the Interconnected Systems in the Northwest to Determine Optimum Methods of Operation—Peak Loads of December 1944

Case Number	System Disturbance*		Clearing Time†		Generators Dropped**			Operating Angles‡ (Degrees)		Remarks
	Fault	Line and Location	Opening	Relaying	Unit	Bus	Time	Initial	Maximum	
Figure 1										
1	2 phase to ground	*Coulee*–Covington—bus *A*	5.75	Carrier				50	105	In cases 1 through 5, occurrence of a fault and the subsequent loss of a line did not result in serious swinging of the machines
2	3 phase	*Coulee*–Covington—bus *A*	5.75	Carrier				50	117	
3	2 phase to ground	*Coulee*–Midway—bus *A*	5.75	Carrier				50	117	
4	2 phase to ground	*Coulee*–Midway—bus *B*	5.75	Carrier				50	109	
5	2 phase to ground	*Coulee*–Spokane—bus *A*	5.75	Carrier				50	96.5	
6	3 phase	*Covington*–Tide Flats—115 kv	9+20.25	Sequential				50	67.5	Covington 115-kv bus not split in case 6
7	3 phase	Midway–*Hanford*—115 kv, 15 miles from Midway	54					50	76	In case 7 only, it was assumed there were two Midway transformer banks so as to obtain the worst fault conditions
Figure 2										
8	2 phase to ground	*Coulee*–Midway—bus *B*	5.75	Carrier	1-*L*	*B*	11.5	61.5	105	
9	2 phase to ground	*Coulee*–Spokane—bus *B*	5.75	Carrier	1-*L*	*B*	11.5	61.5	90	
10	2 phase to ground	*Coulee*–Midway—bus *C*	5.75	Carrier				61.5	Unstable	In case 10 instability occurred between the group of machines containing the generators on Coulee busses *B* and *C* and Washington waterpower system, and the group comprising the remaining system
11	2 phase to ground	*Coulee*–Midway—bus *C*	5.75	Carrier	1-*L*	*C*	11.5	61.5	105	
12	2 phase to ground	*Coulee*–Spokane—bus *C* 230 kv	5.75	Carrier				61.5	101	
13	3 phase	Coulee–*Spokane*—115 kv	9+15.75	Sequential				61.5	104	
14	3 phase	Midway–*Hanford*—115 kv, 15 miles from Midway	54					61.5	154	In case 14 only, it was assumed there were two Midway transformer banks so as to obtain the worst fault condition
Figure 3										
15	2 phase to ground	*Coulee*–Spokane—bus *A*	5.75	Carrier	1-*L*	*A*	11.5	58	105.5	
16	2 phase to ground	*Coulee*–Spokane—bus *C*—230 kv	5.75	Carrier	1-*L*	*C*	11.5	58	99.5	
17	2 phase to ground	*Coulee*–Covington—bus *C*	5.75	Carrier				58	Unstable	In case 17 the Coulee generators on busses *A* and *C* and the Washington waterpower system went out of step from the rest of the system
18	2 phase to ground	*Coulee*–Covington—bus *C*	5.75	Carrier	1-*L*	*C*	17.25	58	135.	In case 18 the generator should have been tripped in 11.5 cycles, but even so the system remained stable
19	2 phase to ground	Coulee–*Midway*—230 kv	4.5		2-*L*	*B*	4.5	58	97.5	Opening of the Coulee–Midway line in case 19 automatically removes two generators from the system
20	3 phase	Midway–*Hanford*—115 kv, 15 miles from Midway	54					58	Unstable	For case 20, even though there was but one Midway transformer bank in service, the slow clearing of the fault caused the two Coulee generators on the Coulee–Midway line to run away from the rest of the system which was stable
Figure 4										
21	2 phase to ground	*Coulee*–Midway—bus *A*	5.75	Carrier	1-*L*	*A*	11.5	59	111	
22	2 phase to ground	*Coulee*–Covington—bus *A*	5.75	Carrier	1-*L*	*A*	11.5	59	94	
23	2 phase to ground	Coulee–*Spokane*—230 kv	5.75		2-*L*	*B*	5.75	59	59	In case 23 two Coulee generators are automatically removed from the system by the opening of the Coulee–Spokane line at Spokane
24	2 phase to ground	*Coulee*–Midway—bus *C*	5.75	Carrier	1-*L*	*C*	11.5	59	99.5	
25	3 phase	*Covington*–Chehalis—230 kv	4.5	Carrier				59	80.5	
26	3 phase	Coulee–*Spokane*—115 kv	9+15.75	Sequential				59	134	
27	3 phase	Midway–*Hanford*—115 kv, 15 miles from Midway	54					59	72	
28	3 phase	*Covington*–Tide Flats—115 kv	9+20.25	Sequential				59	71.5	Covington 115-kv bus not split in case 28

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* Italics denote fault location.

† All times shown are measured in cycles on a 60-cycle base from the instant the fault was initiated.

** Generators tripped by opening transformer breakers. *L* indicates a 108,000-kva generator and *S* indicates a 75,000-kva generator.

‡ Angles were measured between the two machines which were farthest apart at that instant.

Airplane Engine and Propeller Test-Cell Lighting

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AIRPLANE motor and propeller test cell design is relatively new and there has not been sufficient experience in test-cell operation to determine the most practical design. Practically every new cell is designed to differ in many respects from older ones. This condition is particularly true for the lighting systems which vary radically in design for each group of test cells that is built. The author has made a study of existing installations to determine the good and bad practices now in use so that definite and logical improvement can be made in the lighting design of future cells. In all fairness to the engineers who are responsible for the design of present test cells, it must be remembered that there has been no time for detailed study or investigations of the problem. Speed has been the order of the day, and any kind of performance was better than none. Now that we have time to catch our breath and think about doing things better, there has commenced a program of improving the lighting of the existing test cells.

Before discussing the actual mechanics of a satisfactory lighting design, it is most desirable to review briefly from a seeing standpoint the fundamentals of good lighting which have such great importance in test-cell lighting and which have been neglected *entirely* in past test-cell lighting design:

1. The brightness of the work should not be less than that of its immediate surroundings.

2. Foot-candle values are misleading because the workers' eyes do not see foot-candles (incident light) but foot-lamberts (reflected light).

3. In making measurements of light in test cells and control rooms, a photoelectric-cell meter should not be used. An equality-of-brightness meter should be used without a test plate so that readings can be made directly in foot-lamberts.

4. The lighting for the test cell and control room must be designed separately. The two designs must then be co-ordinated, because, while the engine is running, the observer is in the control room (not the test cell).

5. Veiling glare, as the term implies, is a reduction in seeing due to back reflections from a polished surface between the worker's eyes and the work. In the case of test-cell lighting, veiling glare from the double test-cell window may be so bad that the window becomes practically opaque (like a mirror).

6. Measurements on airplane engines show that they have an average reflection factor of 25 per cent which in practice means that when a photoelectric-cell meter shows 100 foot-candles incident on the engine, the worker's eyes see 25 apparent foot-candles (foot-lamberts).

The most common faults found in existing test-cell lighting are:

1. Too little light reflected from the engine both during setup and while on test.

2. Too much light (relatively) in the control room.

3. Poor maintenance of lighting equipment (dirt and oil).

4. Vibration causing disintegration of lighting equipment.

5. Heat breakage of lighting equipment due to poor fixture design.

Illumination Requirements

Test cells require illumination on the motor or propeller from *all* directions for setup work and on the observation window side *only* during test.

In order to meet in a practical way the fundamental specification that the brightness of the work (engine) should not be less than that of its immediate surroundings (control-room work sheets, meters, gauges, and so forth), we must start at the control room and set the minimum illumination for sustained work under the intermittent seeing condition of control-room routine.

An in-service brightness of ten foot-lamberts reflected from the horizontal work sheets, meters, and so forth is sufficient for the seeing task. With a lamp depreciation of 15 per cent, dirt and dust on the lamp, reflector depreciation of 20 per cent, a reflection factor of the work of 80 per cent, and a voltage drop of four per cent (lumen drop of 12 per cent), there will be required a design illumination of $10/(0.85\times0.80\times0.80\times0.88)21$ initial foot-candles.

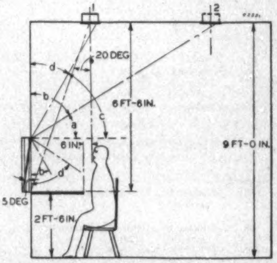

Figure 1. Cross section through typical control room

Mount lighting units as high as possible to keep angles *b* and *d* to a minimum

The initial design illumination for the engine that will result in matching the ten foot-lamberts brightness of the control room is found by applying the following factors: reflection factor of engine 25 per cent; dust and dirt depreciation of lighting equipment after four hours operation, which includes smoke and oil vapor in air and oil and dirt deposit on inner surface of control-room window, 50 per cent; voltage drop four per cent (lumen drop of 12 per cent). No lamp depreciation is allowed because the normal life expectancy of a lamp under the

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sary to trip off load simultaneously with the generation in order to obtain a material improvement in stability. Even with a high percentage of induction-motor load some improvement will be obtained.

When transmission lines can be interleaved, many of the advantages of operating the station solidly bussed are obtained without the high circuit-breaker duty otherwise resulting. General system voltage during a fault is higher than with a solid bus, and the shock of the fault to the system is reduced. Splitting the bus into sections also reduces the effects of bus faults. When compared to an arrangement of isolated lines and generators, less spinning reserve is required for the interleaved system, and transfer of power from one load center to another is more easily obtainable. The effects of faults remaining on a system for relatively long periods because of slow relay operation is less severe for the interleaved system than for the isolated system.

In many ways the interleaved arrangement is a compromise between the solidly bussed and the isolated line and generator arrangements and has many of the advantages of both.

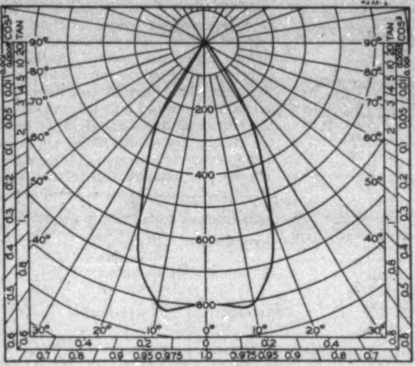


Figure 2. Typical candle-power distribution curve

Number 765—$3^1/_2$-inch focus; no offset
Test number 14,385—100 watts; 1,600 lumens

severe vibration conditions is 200 hours and the lumen depreciation at 200 hours is negligible. The design illumination is (10/0.25×0.50×0.88)90 foot-candles minimum.

Lighting Calculations

The average flux method of calculating illumination cannot be used successfully for either the control room or the engine room, because the average flux method of calculation $\left(\text{foot-candles} = \frac{\text{lumens}}{\text{floor area}} \times \text{utilization factor} \times \text{depreciation factor}\right)$ is based on a uniform distribution of light which is exactly what we do not want for either the control room or engine room.

Control-Room Calculations

Lighting for the control room should be of the same type as that for the engine room, namely, incandescent type. The lighting fixtures should be kept as high as possible, preferably flush in the ceiling, to give the largest angle of incidence possible between the light falling on the window and the normal to the window (see Figure 1), so that the reflected image of the lighting fixture will be below the operators' eyes. The lighting unit should be located with special reference to the log and the instrument table. The ideal location is six inches in front of the edge of the instrument table (see Figure 1). This location assures that the angle A is a maximum consistent with vertical illumination on instruments as far out as the edge of the table. Unless there is additional apparatus on the other side of the control room, it is advantageous not to have additional lighting units in location 2 (see Figure 1). Lights at location 2 deliver light on the window at an unfavorable (low) angle for clear vision through the glass, and the wall opposite the window also is brightly lighted which contributes to the veiling glare in the window. Back reflection (veiling glare) from the window seriously handicaps vision through the window to the engine, and every precaution should be made to prevent this source of annoyance. It is advisable to tilt the glass at an angle of approximately five degrees and to paint all wall, ceiling, floor, and trim surfaces dark gray (reflection factor not over 15 per cent).

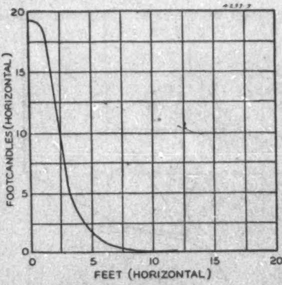


Figure 3. Plot of foot-candle values

Number 765—100 watts; $3^1/_2$-inch focus; no offset
Mounting height—six feet, six inches

The lighting fixture should be of the direct-lighting type with a narrow candle-power distribution of light (see Figure 2) so that the walls will not be too brightly lighted. The lamp size and lateral spacing of the lights along the work table are determined by making a point by point calculation (see Table I) and then plotting the foot-candles against horizontal distance (see Figure 3). Referring to Figure 3, it will be seen that the foot-candle contribution from the unit directly over the work table is 19 foot-candles. The required illumination is 21 foot-candles which leaves (21−19) two foot-candles to be contributed by the two adjacent units (one foot-candle from each unit). Figure 3 shows that at six feet horizontal distance the illumination is one foot-candle. Assuming, therefore, a horizontal spacing of six feet an examination is then made of the illumination at the various intervals between two adjacent units using Figure 3, and a curve of uniformity is plotted as shown in Figure 4 which shows too great a dip in the curve midway between units. The next approach is to determine at what position on the Figure 3 curve (21/2) 11 foot-candles is produced and then to double this distance for the spacing between units and make another examination (full-line curve Figure 4). A 5-foot longitudinal spacing is the correct one as the illumination is essentially uniform at 21 foot-candles. In the instance taken for discussion the particular distribution curve, lamp size, and spacing give the correct results. Had the conditions been changed, other lamp sizes, spacings, and slightly wider or narrower curves could have been employed. In general the best seeing condition will be secured when small lamps, narrow distribution curve, and close spacing are used which is the exact opposite of present lighting practice in control rooms. If necessary local lights should be used for any special instruments, but extreme care must be taken not to increase the initial illumination much over 21 foot-candles. The use of fluorescent lamps in control rooms is not indicated because the color of the light does not match that in the engine room.

Engine-Room Calculations

The cross section of engine rooms varies somewhat, but the methods for calculating the illumination on the engine will be the same for all cross sections. For discussion a control room having a square cross section will be used. The most

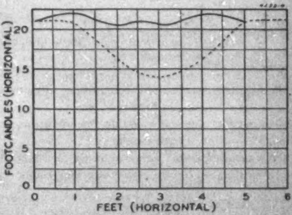


Figure 4. Plot of uniformity of illumination

- - - Illumination uniformity with number 765 on six-foot spacing
—— Illumination uniformity with number 765 on five-foot spacing

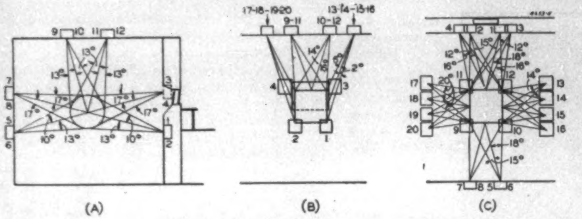


Figure 5. Lighting study of rectangular test cell

A—Section through cell
B—Elevation, window side
C—Plan view of test cell

practical method of computing the normal foot-candles on the engine is to use a modified form of the average-flux method. The distribution curve of the lighting unit is plotted on special rectangular co-ordinate paper on which the candle power is plotted vertically on a linear scale and the angles are plotted horizontally on a scale which is in direct proportion to the solid angle content for the various zones (see Figure 6). The area in square inches under the curve between any angles when multiplied by a constant (750 for the curve shown) gives the lumens in the zone bounded by the two angles. We can determine graphically by plan, cross section, and longitudinal elevation the limits of the angle subtended from any lighting unit by the engine, and then determine the total lumens that will fall on the engine. Knowing the peripheral area of the engine the average illumination on the engine will be total lumens on engine/peripheral area of engine.

Figure 5 shows a cross section, side elevation, and plan of a rectangular test cell having a square cross section of 18 feet with an engine in the center, 4 feet in diameter and 4 feet long. As already discussed, a foot-lambert brightness of at least ten is required which in turn requires an initial foot-candle with a calculated value of at least 90. For the purpose of illustrating the method of foot-candle calculation, 20 lighting units having a light distribution shown in Figure 6 are located in the window wall, opposite window wall, in ceiling for top of engine, and in ceiling for front and back of engine. The vertical and horizontal angles are secured graphically from the cross section, plan, and elevation, and these vertical and horizontal angles are averaged for each unit (when the horizontal and vertical angles differ too greatly, the smaller angles are given double weight in arriving at the average). The various detailed steps of the foot-candle calculation are given as follows:

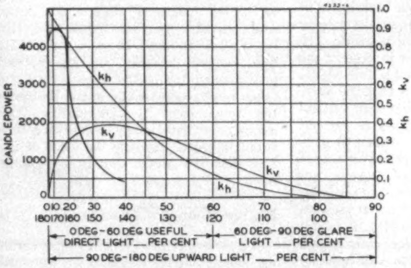


Figure 6. Rectangular graph for plotting illumination

Lamp data—incandescent filament, 500 watts, 115 volts, 9,850 lumens, *PS*-40 bulb

Illumination computation (point-by-point method):

$$\text{Illumination horizontal} = \frac{CP}{h^2} \times k_h$$

$$\text{Illumination vertical} = \frac{CP}{h^2} \times k_v$$

where

Illumination horizontal = horizontal illumination in foot-candles

Illumination vertical = vertical illumination in foot-candle

CP = candle power value at angle considered

h = mounting height above working plane

$\left.\begin{matrix} k_h \\ k_v \end{matrix}\right\}$ = constants (see graph)

Luminous flux computation (for symmetric units):

$F = 750 \times A$ where

F = luminous flux in lumens in zone considered

A = area in square inches under candle power curve in zone considered

FOOT-CANDLES FROM UNITS 1 AND 2

Vertical angle	10 degrees
	13 degrees
Horizontal angle	15 degrees
	18 degrees
Average	$\frac{56}{4} = 14$ degrees

Area in square inches under curve (Figure 6) 0–14-degrees zone = 4.36 × 0.25 inch = 1.085 square inches. Lumens in 0–14-degrees zone = 1.085 × 750 = 816 lumens from one unit; 816 × 2 = 1,632 lumens from two units.

Area of engine toward window = $\pi D = \pi 4 =$ 12.5 square feet

$$\text{Foot-candles} = \frac{1{,}632}{12.5} = 130$$

FOOT-CANDLES FROM UNITS 3 AND 4

Vertical angle	17 degrees
	17 degrees
Horizontal angle	12 degrees
	16 degrees
Average	$\frac{62}{4} = 15.5$ degrees

Area in square inches under curve (Figure 6) 0–15.5-degrees zone = 4.36 × 0.28 = 1.22 square inches. Lumens in 0–15.5-degrees zone = 1.22 × 750 = 915 lumens from one unit; 915 × 2 = 1,830 lumens from two units.

Area of engine toward window = $\pi D = \pi 4$ = 12.5 square feet.

$$\text{Foot-candles} = \frac{1{,}830}{12.5} = 146$$

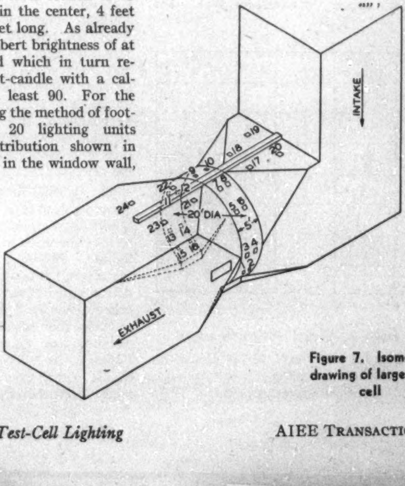


Figure 7. Isometric drawing of large test cell

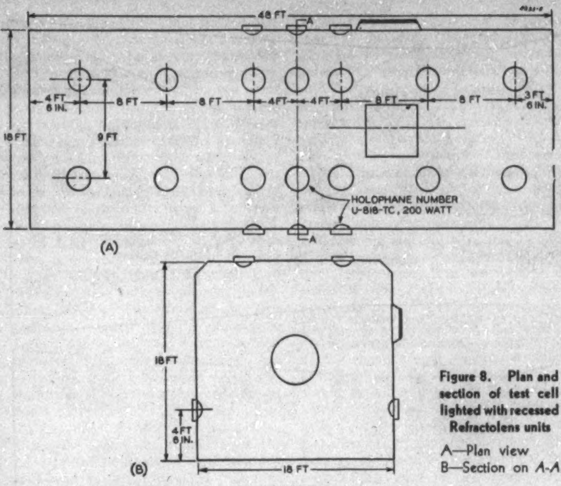


Figure 8. Plan and section of test cell lighted with recessed Refractolens units

A—Plan view
B—Section on *A-A*

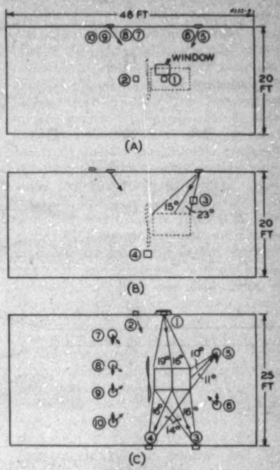


Figure 9. Test-cell lighting using two types of lighting units

Unit numbers 1, 2, 3, 4; number 7474-1, 300 watt
Unit numbers 5, 6, 7, 8, 9, 10; number *U*-818-*TC*, 200 watt
A—Elevation of window wall
B—Elevation of wall opposite window
C—Plan view of test cell

Total foot-candles on engine toward window (130 plus 146) 276.

Foot-candles From Units 5 and 6

Vertical angle	10 degrees
	13 degrees
Horizontal angle	15 degrees
	18 degrees
Average	$\frac{56}{4} = 14$ degrees

Area in square inches under curve (Figure 6) 0–14-degrees zone = 4.36 inches × 0.25 inch = 1.085 square inches. Lumens in 0–14-degrees zone = 1.085 × 750 = 816 lumens from one unit; 816 × 2 = 1,632 from two units.

Area of engine $= \pi D = \pi 4 = 12.5$ square feet

$$\text{Foot-candles} = \frac{1{,}632}{12.5} = 130$$

Foot-candles From Units 7 and 8

Vertical angle	17 degrees
	17 degrees
Horizontal angle	15 degrees
	18 degrees
Average	$\frac{6}{4} = 16.75$ degrees

Area in square inches under curve (Figure 6) 0–16.75-degrees zone = 4.36 inches × 0.35 inch = 1.53 square inches.

Lumens in 0.16175-degree zone = 1.53 × 750 = 1,150 lumens from one unit; 1,150 × 2 = 2,300 lumens from two units.

Area of engine $= \pi D = \pi 4 = 12.5$ square feet

$$\text{Foot-candles} = \frac{2{,}300}{12.5} = 184$$

Total foot-candles on engine on side away from window (130 plus 184) 314.

Foot-candles From Units 9, 10, 11, 12

Vertical angle	13 degrees
	13 degrees
Horizontal angle	14 degrees
	16 degrees
Average	$\frac{56}{4} = 14$ degrees

Area in square inches under curve (Figure 6) 0–14-degrees zone = 4.36 inches × 0.25 inch = 1.085 square inches. Lumens in 0–14-degree zone = 1.085 × 750 = 816 rom one unit; 816 × 4 = 3,264 lumens from four units.

Area of engine $= \pi D = \pi 4 = 12.5$ square feet

$$\text{Foot-candles} = \frac{3{,}264}{12.5} = 260 \text{ on top of engine}$$

Foot-candles From Units 13 and 16

Vertical angle	20 degrees	
	20 degrees	
Horizontal angle	6 degrees	double weight
	2 degrees	
	6 degrees	
	2 degrees	
Average	$\frac{56}{6} = 9.3$ degrees	

Area in square inches under curve (Figure 6) 0–9.3-degrees zone = 4.2 inches × 0.1 inch = 0.42 square inch. Lumens in 0–8.33-degree zone = 0.42 × 750 = 315 from one unit; 315 × 2 = 630 from two units.

Area of end of engine $= \frac{\pi d^2}{4} = \frac{\pi 16}{4} = 12.5$ square feet

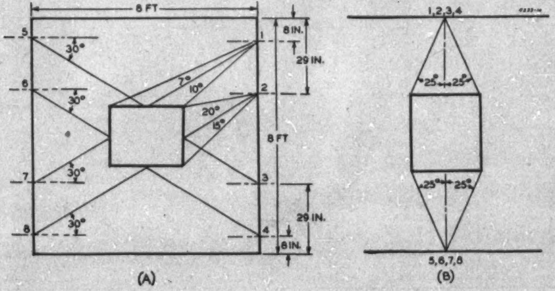


Figure 10. Plan and section of test cell showing beam throw and spread of the lighting units

A—Section through test cell
B—Plan view of test cell
Lighting units numbers 1 to 10 are number *U*-818-*TCHS*, 300 watts

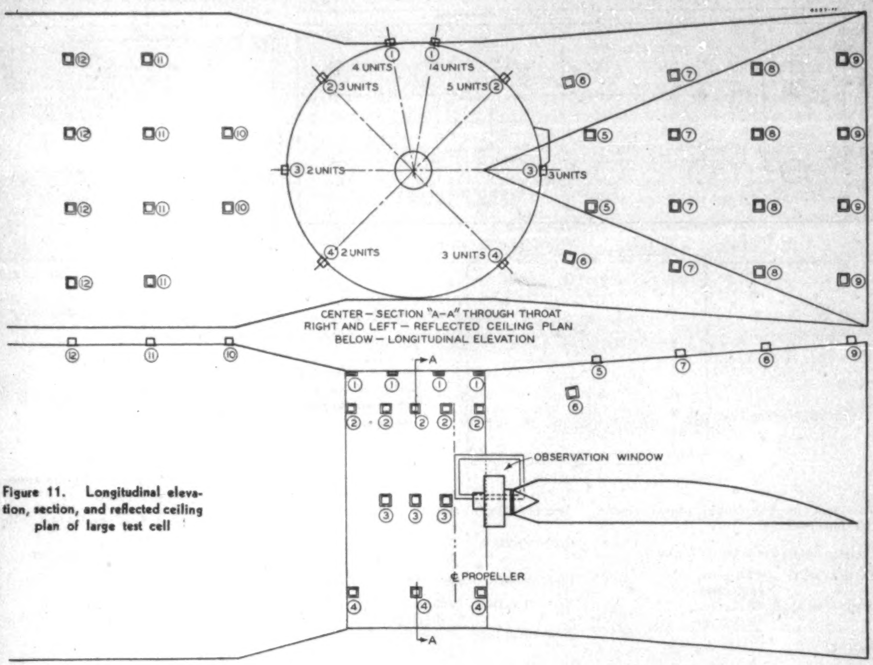

4 UNITS
4 UNITS
3 UNITS
5 UNITS
2 UNITS
3 UNITS
2 UNITS
3 UNITS
CENTER – SECTION "A–A" THROUGH THROAT
RIGHT AND LEFT – REFLECTED CEILING PLAN
BELOW – LONGITUDINAL ELEVATION
OBSERVATION WINDOW
℄ PROPELLER
A
A
①
②
③
④
⑤
⑥
⑦
⑧
⑨
⑩
⑪
⑫

Figure 11. Longitudinal elevation, section, and reflected ceiling plan of large test cell

$$\text{Foot-candles} = \frac{630}{12.5} = 50$$

FOOT-CANDLES FROM UNITS 14 AND 15

Vertical angle 20 degrees
20 degrees
Horizontal angle 6 degrees
2 degrees
6 degrees
2 degrees } double weight
56

$$\text{Average} \quad \frac{56}{6} = 9.33 \text{ degrees}$$

Area in square inches under curve (Figure 6) 0–9.33-degree zone = 4.3 inches × 0.12 inch = 0.53 square inch. Lumens in 0–9.33-degree zone = 0.53 × 750 = 390 from one unit; 390 × 2 = 780 lumens from two units.

Area of end of engine $\frac{\pi d^2}{4} = \frac{\pi 16}{4} = 12.5$ square feet

$$\text{Foot-candles} = \frac{780}{12.5} = 62$$

Total foot-candles on propeller end of engine (50 plus 62) 112.

FOOT-CANDLES FROM UNITS 17, 18, 19, 20

Total foot-candles on rear end of engine 112. The same as from units 13, 14, 15, 16.

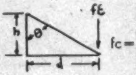
The foot-candles on sides and top of engine average [(276+314+260)/3]283, which is too high. The ratio of lumens for a 300-watt and –500 watt lamp is (5,650/9,850)0.575. The ratio for a 200- and 500-watt lamp is (3,650/9,850)0.372. If the units were lamped with 300-watt lamps the foot-candles would be (283 × 0.575) 162 and for 200-watt lamps would be (283 × 0.372) 104 which is near enough to the 90 minimum foot-candles required. Units 1 to 12 inclusive should be lamped with 200-watt lamps and units 13 to 20 inclusive with 500-watt lamps. It would not be advisable to reduce the number of units because no allowance has been made for lamp failure during test and for this reason, as well as for the reason of better diffusion, a multiplicity of smaller wattage units is preferable to a smaller number of larger wattage units.

For engine setup and inspection all units should be burning. For test inspection through window from control room units 1, 2, 3, 4, 9, and 11 should be burning.

The relatively small angles included by the engine from the lighting units rules

Table I. Point-by-Point Calculation

Unit Number—765–100 Watts, 3 1/2 Inches Focus, No Offset
Mounting Height—Six Feet, Six Inches
Test Number of Curve—14,385



$$fc = \frac{cp}{h^2} \cos^3 \theta$$

d	h	$\frac{d}{h}$	θ	cp	h^2	$\cos^3 \theta$	fc
0	6.5	0	0	805	42.2	1	19.1
1	6.5	0.154	8.8	820	42.2	0.96	18.8
2	6.5	0.308	17.1	630	42.2	0.875	13.0
4	6.5	0.615	31.6	222	42.2	0.62	3.2
6	6.5	0.922	42.7	90	42.2	0.40	0.85
8	6.5	1.25	50.9	61.5	42.2	0.25	0.365
10	6.5	1.54	53.6		42.2		
12	6.5	1.84	61.5	37	42.2	0.11	60.09

out the use of mercury lamps because due to the large light source accurate control is impractical. Also mercury lamps can be burned only base up or base down which rules them out for many test-cell locations. Mercury lamps alone would have an interval of darkness of about six minutes from a momentary voltage interruption. The stroboscopic effect of the mercury lamps might be annoying due to the moving parts of the engine. Units designed for mercury lamps to operate base up or base down not only are inefficient but also often become so large that there is not sufficient depth of concrete to permit flushing (this is especially true for ceiling and radially mounted units). These inherent drawbacks of mercury lamps in test-cell lighting design are pointed out because they have been used in several test cells in the belief that something in the color helped the already unsatisfactory installation and in an effort to prolong the life of the lamp. To maintain the brightness balance between the engine and control room the color of the light should be essentially the same for each location. Mercury lamps are *not* desirable for test-cell lighting.

The walls and ceiling of the engine room should be painted gray and have a reflection factor not greater than 25 per cent (same as engine). Contrary to general opinion, dark walls and ceiling do not increase the illumination more than approximately 10 per cent for direct lighting with narrow-spread lighting units.

Mechanical Requirements

Test-cell lighting equipment is subjected to severe vibration which is comparable to the vibration of the engine or propeller itself and the greater the horsepower of the engine the greater is the vibration to which the units are subjected. To overcome maintenance problems due to vibration, all parts of test-cell units must be of heavy generous construction. All bolts, screws, and nuts must be of generous size and of vibration-proof airplane construction. Consonance of the volume of air inside the unit will occur at some critical engine speed which will, if prolonged, result in lamp breakage. To overcome this consonant effect the design of the unit must be such that at normal testing speeds (2,300 to 2,700 rpm) consonance does not occur. A relatively large (2 inch to 3 inch) opening to outside air will reduce the free period of the unit by one half and is the most practical method of detuning the unit to normal engine speeds. Vibration transmitted through the socket to the lamp should be reduced to safe limits by a vibration-absorbing mounting of the socket for low-horsepower engines and by eliminating any metal connection between the front of the unit and the socket for engines of high horsepower. Shock-proof sockets are not satisfactory because these are designed to reduce shocks (low frequency), whereas in test-cell operation comparatively high-frequency vibration corresponding to engine speeds must be dealt with. It has been found that the most effective means of reducing vibration transmitted to the lamp is to detune the unit, to mount the socket assembly entirely apart from the front of the unit, and to make all parts of heavy cast construction.

Adequate gasketing must be used to prevent oil and oil vapor from entering the unit, as such deposits on the lamp and front glass cover quickly will reduce the amount of illumination on the engine and will cause overheating of the lamp and front glass cover by absorption of light and its conversion to heat. Overheating quickly bakes on the oil on the outside of the glass cover and further reduces the illumination. Tests show that the normal oil and dirt deposit on the front of the glass of the lights and on the glass of the observation window and the absorbing effect of the oil vapor and exhaust gases in the air will reduce the illumination on the engine by 50 per cent in four hours of engine operation. Not only must the front opening be gasketed properly, but also the gasket construction must stand frequent servicing without depreciation. The front cover glass must be cemented permanently into the face plate with a cement that will not rupture or depreciate under the action of vibration, oil, and heat and will allow the necessary expansion and contraction of the glass from alternate heating and cooling without breaking the seal. The lens for controlling the direction and spread of the light must be held securely in the face plate and gasketed to reduce vibration and uneven stress. The lens and cover glass must not be in contact with each other, and both must be able to withstand a mechanical shock of eight foot-pounds (a one pound steel ball dropped on the glass from a height of eight feet) and a thermal shock represented by a sudden change in temperature of not less than 100 degrees centigrade. It has been found in practice that at *least* these mechanical and thermal shock specifications are required for trouble-free operation. The cover glass should be of a homogeneous type (not laminated).

Full life expectancy of 1,000 hours cannot be expected from lamps operating under the severe vibration condition of test-cell use, and a life of 300 hours should be considered normal for the service. This means that lamps will have to be replaced more often than for ordinary lighting units, and for this reason test-cell units should be constructed so that

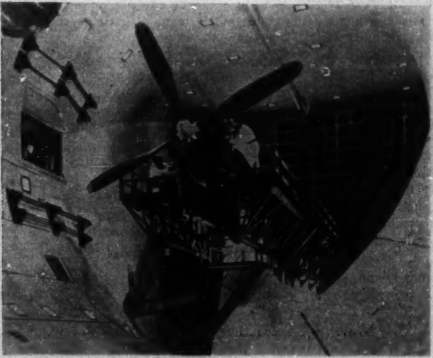


Figure 12. A most satisfactory propeller test-cell lighting installation (Albert Kahn, Inc., engineers)

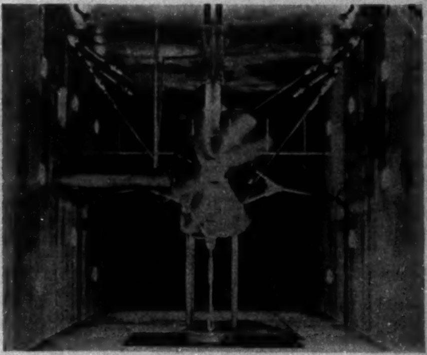


Figure 13. A satisfactory lighting arrangement for a rectangular engine-test room

Cable Used for Transmitting Electric Energy in Airplanes

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Synopsis: A method is proposed for determining the current capacity and the temperature of operation of cables in airplanes. The cable selected should in combination with the power plant result in the smallest weight for each electrical unit converted into useful work. A general equation is given for selecting the optimum current which is expressed in terms of the weight of the cable, power plant, and fuel; the length and resistance of the circuit, and the operating voltage of the system. The method of selection indicates that the current capacity of a cable operated for short periods without requiring additional power-plant or fuel capacity is limited by the temperature the insulation will withstand. For open wiring requiring power-plant capacity bare copper conductors should operate from 50 to 65 degrees centigrade above ambient temperature, and insulated copper conductors 10 to 25 degrees centigrade lower. The temperature of the conductors in conduit will depend upon the installation. Measurements made upon two samples of cable in 0.5-inch conduit indicated that the conductors should operate at 45 to 80 degrees centigrade above ambient temperature. All bus bars and conduit should be enameled or painted to reduce their operating temperatures. Measurements upon an aluminum-alloy tube indicated a drop of 35 degrees centigrade in 100 degrees. The greatest reduction in weight of the electric system will be accomplished by increasing the voltage. Further reductions may be accomplished by using aluminum conductors. If strip conductors are used terminal connectors may be eliminated and the connecting of aluminum conductors simplified.

CABLE used for transmitting electric energy in airplanes may be divided into low-voltage and high-voltage cable. Low-voltage cable is used for transmitting energy at voltages less than 220. High-voltage cable is used for transmitting energy at voltages high enough to cause spark discharges. Some of the cable manufactured for low-voltage operation will withstand 15,000 volts, but in the following treatment high-voltage cable will be considered as that type of cable fabricated primarily to be operated at high voltages.

Because of the use of low voltages on airplanes there are circuits which must transmit heavy currents to deliver the required amount of energy. The selection of 24 volts was made a few years ago, and in a paper[1] by Lieutenant Commander Grant and Melville F. Peters it was shown to be the optimum voltage for airplanes using a storage battery supplying part of the peak load. When the peak load is supplied by the generator the weight of the electric system will decrease as the voltage is increased. For the same percentage loss in power the cross section of the conductor and consequently the weight will decrease as the square of the voltage. This relation between voltage and weight is well known, and perhaps because of what is called conservative practice the possible saving in weight of the electric system by increasing the voltage has not kept pace with the growth of the airplane. In the past it has been determined by what is considered safe for military operation, and it is beyond the scope of this investigation to consider what may happen to the electric system in combat if shrapnel pierces a cable carrying what is considered high voltage for airplanes. However, it is possible to conduct an investigation of electric cable in such a way that the results are applicable to a wide range of voltage. Equipment of suitable frequency was not available to continue the study with alternating current. However, the general experimental procedure is applicable to either alternating or direct current.

Until recently high-voltage ignition cable was designed to conduct the energy from the spark generator to the spark plugs, and little attention was given to its effect upon the character of the spark discharge. Ignition cable today is designed to add as little as possible to spark-plug electrode wear, to suppress radiation from the ignition system, to serve as a means of supercharging the spark plugs, and to require the smallest amount of energy from the spark generator to fire the plugs without interfering with engine performance. Since most ignition-cable failures are caused by the insulation operating at high ambient temperatures, much attention must be given to the temperature range of the insulating material.

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relamping is simply and quickly accomplished. Advantage should be taken of the relatively short life expectancy by using 115-volt lamps on 125-volt circuits which would give 132 per cent foot-candles and a life expectancy of 350 hours. Lamp filaments are broken more easily when cold than hot. It is suggested that they be switched so that, when in the "off" position sufficient current will flow to keep the filaments red hot. Some industrial plants use this method of prolonging the lamp life where vibration exists.

Installation Methods

Lighting equipment should be flushed in the concrete wherever possible, because the masonry helps to dampen vibration and because flushed-in units do not interfere with the air flow. It is not practical to make any changes in a flushed-in job after the job is completed so it is important that full advantage be taken of past mistakes and fundamental knowledge to design new test-cell lighting to give satisfactory service. When flushed-in lighting equipment cannot be used, such as in relighting existing test cells, it will be necessary to use exposed mounted equipment. Such equipment should be as shallow as possible so that its projection from the walls and ceiling is a minimum.

There are three lighting systems for complete test cell:

1. The control room.
2. Engine setup.
3. Engine inspection from control room through observation window.

Each system should be switched separately from the control room.

In making a test-cell layout for the engine room it is better to use a larger number of smaller lamps than a few large lamps, and it is desirable to design for 25 per cent higher foot-candles than the initial calculated minimum of 90 because some lights may fail during test, and cleaning maintenance is usually very poor in actual operating practice.

Figures 7, 8, 9, 10, and 11 show arrangements of lighting equipment taken from actual practice. A calculation of the foot-candles for each, shows that, with the exception of Figure 11, the illumination is likely to fall below the calculated initial minimum of 90 foot-candles.

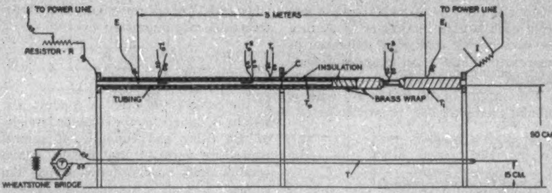

Figure 1. Arrangement of apparatus for measuring the current capacity of cable in open wiring

General Requirements for the Selection of Conductors Operating at Low Voltage

Optimum weight of the electric system will be obtained with the selection of a group of conductors which in combination with the power plant affords the smallest weight for each electrical unit transformed into useful work. Such a treatment resolves itself into investigating the following:

1. The weight of the power plant.
2. The weight of fuel required for each kilowatt-hour of electric power developed.
3. The portion of the power at peak load to be taken from the generator and the portion from the battery.
4. The group of conductors supplying equipment which requires no addition to the power unit or the fuel supply.
5. The group of conductors supplying equipment which requires addition to the power plant.
6. The group of conductors supplying equipment which requires addition to the power plant and the fuel supply.

Analysis of Factors Affecting Weight of Low-Voltage Electric System

The weight of the power plant will depend upon the weight of the generator and the source of power. If the generator is driven from the main engine the weight of the engine multiplied by the percentage of power taken from the main engine should be added to the weight of the generator. If an auxiliary engine is used, the weight of this power installation must be added to the weight of the generator. In the computations used throughout this paper, the weight of the generator was taken as 6.6 pounds per kilowatt, and an additional weight of 6.6 pounds was added for the source of power. This is low for auxiliary-power-plant units and is approximately correct for main-engine-driven generators.

Since the unit of electric power is the watt, it will be found convenient to express the weight of the power equipment required to generate a unit of electric power as the weight in grams rather than the weight in ounces or pounds. For the value of 13.2 pounds selected for each kilowatt generated the simpler relation may be used that it requires six grams of power-plant weight for each watt produced by the generator to supply the losses in the cable. The amount of fuel required for continuously operated equipment depends upon the type of service and is taken as two grams of fuel for each watt of electric energy produced throughout the flight. These values cannot be true for all types of service but are used as a basis for the calculations. The losses in the conductors are expressed in watts, and here again it is convenient to multiply the loss in watts by six or eight which gives the portion of weight of the power-plant unit and fuel which is dissipated by the conductor and cannot be utilized as useful power. For these reasons all weights of conductors are expressed in grams per meter so that the portion of the weight of the power plant which must be utilized to supply the losses in the conductor can be added without change of units to the weight of the conductor.

Storage batteries are used in aviation for standby purposes and for supplying energy in combination with the generator during peak loads. The size of a storage battery for standby purposes is determined by the needs of the service, and the uses which make it necessary cannot be evaluated in terms of minimum weight. Such uses as lighting, signal purposes, and radio operation, may make a battery indispensable; the discussion which follows, however, deals only with the use of the battery for supplying energy during peak loads. The size of the battery to be used in combination with the generator will be that size which makes the combined weight of the two a minimum, and, as shown in Appendix *A*,* its use cannot be justified from the standpoint of optimum weight. For this reason the storage battery will receive no further consideration.

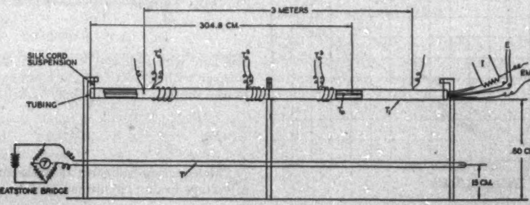

Figure 2. Arrangement of apparatus for measuring the current capacity of cable in conduit

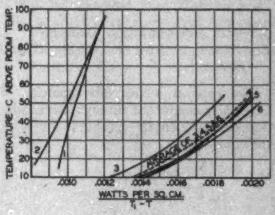

Figure 3

Curve 1—Bare copper tube—outside diameter 0.498 inch

Curve 2—Copper tube—one layer cotton tape, three coats varnish, brass wrap—outside diameter 0.536 inch

Curve 3—Copper tube—one layer cotton tape—outside diameter 0.524 inch

Curve 4—Copper tube—one layer cotton tape, one coat varnish—outside diameter 0.528 inch

Curve 5—Copper tube—one layer cotton tape, two coats varnish—outside diameter 0.531 inch

Curve 6—Copper tube—one layer cotton tape, four coats varnish, carbon dust—outside diameter 0.532 inch

Dotted line is average of curves 3, 4, 5, 6

The rate of heat generation per unit length of the cable by a current i is given by the equation:

$$H_0 = \frac{i^2\rho(1+\alpha T_0)}{\pi(r_1^2 - r_0^2)} = E_0 i \text{ watts} \tag{1}$$

where

ρ = the resistivity of the conductor
α = the temperature coefficient of resistivity
T_0 = the temperature of the conductor
r_1 = radius of the hollow conductor

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* Recently batteries have been developed which give a greater output in kilowatt-hours per kilogram of weight than the values used in Appendix *A*, but these data are not available to the authors. For those to whom the information is available the values may be substituted in the equations and the advisability of the battery considered.

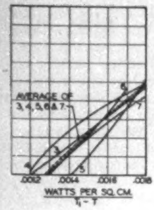


Figure 4

Curve 3—Copper tube—one layer glass tape—outside diameter 0.523 inch

Curve 4—Copper tube—one layer glass tape, one coat varnish—outside diameter 0.524 inch

Curve 5—Copper tube—one layer glass tape, two coats varnish—outside diameter 0.525 inch

Curve 6—Copper tube—one layer glass tape, three coats varnish—outside diameter 0.528 inch

Curve 7—Copper tube—one layer glass tape, four coats varnish—outside diameter 0.529 inch

Dotted line is average of curves 3, 4, 5, 6, 7

r_0 = inner radius of hollow conductor

E_0 = voltage drop per unit length of conductor

The rate at which this quantity of heat H_0 is conducted through the insulation per unit length of cable is given by the equation,

$$H_0=\frac{2\pi K(T_0-T_1)}{\log\frac{r_1+t}{r_1}}\text{ watts} \tag{2}$$

where K is the effective thermal conductivity of the insulation and protective coating surrounding the conductor having a thickness t.

T_1 is the temperature of the outer surface of the cable.

To be in equilibrium H_0 must equal the rate at which heat is lost per unit length from the surface of the cable. This rate of loss of heat is expressed by the equation,

$$H_0=2\pi e(r_1+t)(T_1-T)l\text{ watts} \tag{3}$$

where e is the rate at which heat is lost from each square centimeter of surface for each degree centigrade difference in temperature, and T is the ambient temperature.

The weight of the cable W_c with a tubular conductor may be written as

$$W_c=\frac{\delta_1}{2\pi B}\left\{\frac{\log\frac{r_1+t}{r_1}}{K}+\frac{1}{e(r_1+t)}\right\}\times l+\pi\delta_2 t(2r_1+t)l \tag{4}$$

and for a rectangular conductor or strip

$$W_s=\frac{\delta_1}{B_1}\left\{\frac{1}{2K(a+b+2t)}+\frac{1}{2e(a+b+4t)}\right\}\times l+2t\delta_2(a+b+2t)l \tag{5}$$

where

$$B=B_1=\frac{T_0-T}{i^2\rho(1+\alpha T_0)}$$

a = width of conductor
b = thickness of conductor
l = length of conductor
δ_1 = density of conductor
δ_2 = density of insulation

The rate at which the energy H is developed by the power plant for a circuit requiring i amperes is Ei where E is the voltage at the bus bars. The rate at which the energy is expended in a cable of length l is H_0l so that $H-H_0l$ is the rate at which energy is delivered. If the cable weighs W grams per unit length the watts delivered for each gram of weight of cable is

$$H_w=\frac{H-H_0l}{Wl}=\frac{\frac{Ei}{l}-i^2R}{W} \tag{6}$$

If K_2 grams are allowed for each watt generated by the power-plant installation the number of watts delivered per gram of weight may be written

$$H_x=\frac{\frac{Ei}{l}-H_0}{W+K_2\frac{Ei}{l}}=\frac{\frac{E}{l}-iR}{\frac{W}{i}+K_2\frac{E}{l}} \tag{7}$$

where the resistance $R=R_0(1+\alpha T_0)$. Putting the derivative of H_x with respect to i equal 0, the optimum current capacity for a cable may be written

$$i=-\frac{Wl}{K_2E}\pm\sqrt{\left(\frac{Wl}{K_2E}\right)^2+\frac{W}{RK_2}} \tag{8}$$

If the cable is enclosed in conduit, the weight W designates the combined weight of the cable and conduit. If several cables are enclosed, the weight of the conduit must be distributed among the cables.

The difference in temperature between the surface of the cable and the ambient temperature may be obtained from the equation,

$$T_1-T=(T_0-T_1)\left\{1-\frac{\log\frac{r_1+t}{r_1}}{\log\frac{r_1+t}{r_1}+\frac{K}{e(r_1+t)}}\right\} \tag{9}$$

where the values assigned to e must be taken to correspond with T_1-T. After selecting these values of e the rate of heat lost H_0 per unit length may be calculated for a range of values for T_0-T from the equation,

$$H_0=\frac{2\pi(T_0-T)}{\left(\frac{\log\frac{r_1+t}{r_1}}{K}+\frac{1}{e(r_1+t)}\right)} \tag{10}$$

Table I. Percentage Decrease in Crest Current Between Two Oscillations

Resistance in Ohms	Percentage Decrease in Crest Current
0	0
2	6
6	17
20	47
100	97

When these values are substituted in equations 6 and 7 and plotted versus T_0-T the optimum value for temperature of operation above ambient temperature of the cable is indicated.

The weight of the cable and portion of the power plant supplying i amperes may be expressed in the form,

$$W=\left\{\frac{i\rho\delta_1(1+\alpha T_0)}{E_0}+\pi t\delta_2\left(2\sqrt{r_0^2-\frac{i\rho(1+\alpha T_0)}{\pi E_0}}+t\right)\right\}\times l+K_2(E+E_0)i \tag{11}$$

For bare wire $t=0$ and if the derivative is taken with respect to E_0 and put equal to zero the drop in voltage may be written

$$E_0=l\sqrt{\frac{\rho(1+\alpha T_0)\delta_1}{K_2}} \tag{12}$$

This equation indicates that the drop in voltage in any circuit is equal to the

Figure 5

Curve 1—Bare copper tube—outside diameter 0.247 inch

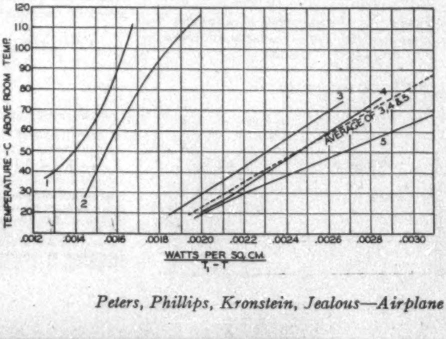
Curve 2—Copper tube—one layer cotton tape, 11 coats light varnish, two coats heavy varnish, metal wrap (brass)—outside diameter 0.293 inch

Curve 3—Copper tube—one layer cotton tape—outside diameter 0.287 inch

Curve 4—Copper tube—one layer cotton tape, two coats varnish—outside diameter 0.287 inch

Curve 5—Copper tube—one layer cotton tape, 11 coats light varnish, one coat heavy varnish—outside diameter 0.293 inch

Dotted line is average of curves 3, 4, 5



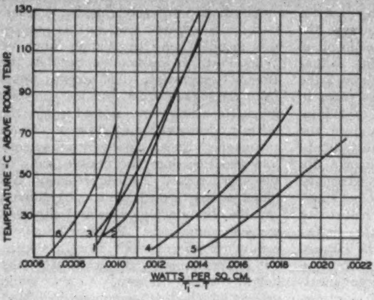


Figure 6

Curve 1—Bare copper strip—0.5x0.063 inch—width horizontal

Remaining curves width vertical

Curve 2—Bare copper strip—0.5x0.063 inch

Curve 3—Copper strip—one layer cotton tape, three heavy coats varnish, brass wrap—0.542x0.122 inch

Curve 4—Copper strip, one layer cotton tape—0.525x 0.095 inch

Curve 5—Copper strip—one layer cotton tape, three heavy coats varnish—0.540x0.103 inch

Curve 6—Bare copper strip—2.0x0.003 inch

length multiplied by the square root of the product of conductivity by the density, divided by the weight in grams for each watt generated, or it is equal to a constant multiplied by the length of the cable. Now it is obvious that for a lighting circuit the drop in voltage should not be proportional to the length of lead, for a light near the source of supply should receive the same voltage as the one on the tip of the wing. To obtain an optimum voltage drop which should hold for all circuits we may follow the method used in obtaining the weighted mean. Suppose, for example, a calculation using equation 12 indicates a drop of one volt for 100 pounds of cable, a drop of five volts for 200 pounds of cable, a drop of eight volts for 300 pounds of cable, and a drop of ten volts for 400 pounds of cable, the total weight of cable is 1,000 pounds.

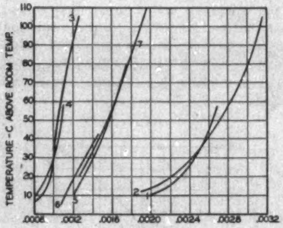


Figure 7

Curve 1—Number-10 vinylite-insulated braid-lacquered cable

Curve 2—Number-8 vinylite-insulated braid-lacquered cable

Curve 3—Aluminum tube heated by passing current through tube

Curve 4—Aluminum tube heated by passing current through inclosed cable

Curve 5—Same as curve 3 except tubing covered with enamel

Curve 6—Same as curve 4 except tubing covered with enamel

Curve 7—Same as curve 3 except tubing covered with carbon black in varnish

The weighted mean voltage drop is

$$E = \frac{(100\times1)+(200\times5)+(300\times8)+(400\times10)}{100+200+300+400} = 7.5 \text{ volts}$$

The actual weight of cable in each circuit which must be used to obtain this average drop is

$$W_1 = 100\times\frac{1}{7.5} = 13.34$$

$$W_2 = 200\times\frac{5}{7.5} = 133.4$$

$$W_3 = 300\times\frac{8}{7.5} = 320.0$$

$$W_4 = \frac{400}{1{,}000}\times\frac{10}{7.5} = \frac{535.5^{*}}{1{,}000}$$

From the foregoing it is apparent that there is no increase in the total weight of cable if the mean voltage drop is used instead of the optimum for each circuit.

If the values of ρ and δ are substituted in the equation with $K_2 = 8$ grams per watt generated, the drop in voltage for aluminum, copper, and iron may be written

E_0 aluminum = 0.098 volt per meter at 25 degrees centigrade

E_0 copper = 0.137 volt per meter at 25 degrees centigrade

E_0 iron = 0.313 volt per meter at 25 degrees centigrade

Factors Affecting the Performance of High-Voltage Cable

The following discussion will be limited to cable used for ignition. The character of the spark discharge most suitable for internal-combustion-engine operation will be that which will ignite a combustible mixture under all conditions of operation, require a minimum expenditure of electric energy, cause the smallest amount of electrode wear, reduce radio interference, and permit air for supercharging the spark plugs to be carried through the conductor. This freedom of selection is permitted since many investigations have shown that within reasonable limits the character of the spark does not affect the performance of the engine.

The energy H in joules required to cause a spark discharge is given by the equation,

$$H = {}^1/_2CE^2 \tag{13}$$

where C is the capacitance of the secondary circuit in farads and E the breakdown voltage of the spark gap. Since E is determined by the mass of gas between the electrodes which is determined by the cylinder pressure, temperature, and electrode separation, the only means by which H may be decreased is by decreasing C. This is accomplished by increasing the size of the metal tubing surrounding the conductor, decreasing the dielectric constant of the insulation, or decreasing the size of the conductor. Increasing the size of the tubing is objectionable, because it increases the weight. Decreasing the dielectric constant has been carried as far as is consistent with good thermal and electrical properties of the insulating compound. Decreasing the size of the conductor is possible and has been reduced to 0.04 inch. Below this the mechanical strength is low, and the electrical gradient increases very rapidly so that failures may be expected with any appreciable decrease in diameter of the conductor below 0.04 inch. The Navy type of seven-millimeter cable with a one-millimeter conductor has a ratio of conductor to shielding of $\frac{1}{e^2}$. This value was selected after observing that it may be obtained by putting the derivative of the product of the capacitance and electrical gradient equal to zero.

Table II. Wear of 18 Spark Plugs After 500 Hours Operation at Room Temperature

Electrode Wear in Inches

Resistance of Resistor (Ohms)	Stainless-Steel Cable—Capacitance 22×10^{-12} (Farads Per Foot)	Copper-Conductor Cable—Capacitance 45×10^{-12} (Farads Per Foot)
None	0.055	0.110
100	0.003	0.003
500	0.002	0.003
1,000	0.002	0.003
5,000	0.002	0.002
10,000	0.002	0.002
25,000	0.001 −	0.001 −
50,000	0.001 −	0.001 −
70,000	0.001 −	0.001 −

When a capacitance C discharges through an inductance L and resistance R, the current at time t after the beginning of the discharge may be given by

$$i = \frac{E}{\omega L}\, e^{-\delta t} \sin \omega t \tag{14}$$

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* In Appendix *B* this is shown to hold for the general case.

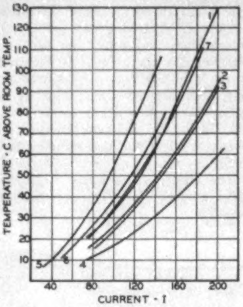


Figure 8. Shows the temperatures of bare and covered conductors

Curve 1—Bare copper strip
Curve 2—Copper strip—cotton wrap
Curve 3—Bare copper tube—outside diameter 0.498 inch
Curve 4—Copper tube and cotton wrap—outside diameter 0.524 inch
Curve 5—Aluminum tube bare—outside diameter 0.540 inch
Curve 6—Aluminum tube, one coat of white enamel—outside diameter 0.540 inch
Curve 7—Aluminum tube, one coat of carbon black in varnish—outside diameter 0.540 inch

where

$$\delta = \frac{R}{2L} \qquad \omega = \sqrt{\frac{1}{CL} - \frac{R^2}{4C^2}}$$

and E is the voltage of the capacitance at the beginning of discharge. The damping in equation 14 is given by the term,

$$e^{-\delta t} = e^{-\frac{Rt}{2L}} \tag{15}$$

Table I shows the percentage decrease in crest current between two oscillations computed from equation 15 for a capacitance of $C = 2 \times 10^{-10}$ farads, and $L = 2 \times 10^{-6}$ henrys, which corresponds closely to the ignition cable in a shielded harness.

Both radio interference and spark-plug wear are decreased by increasing the resistance of the secondary circuit. The spark-plug wear is decreased because the energy which is normally dissipated in the spark-plug gap is dissipated in the conductor of the cable. For short-wave lengths compared to the harness length the present type of radiation suppression commonly called shielding is not adequate without sufficient resistance to make the circuit aperiodic. For long wave lengths the harness shielding is effective, because the current flowing in the cable produces a field equal to and in the opposite direction to the field produced by the return current in the harness. The return of the current through the harness rather than the engine is explained by the proximity effect. When the wave lengths are short compared to the harness, the current in the conductor is not in phase with the current in the shielding, so that part of the return current is through the engine. Radiation then takes place in such a way that the sum of the fields from the currents in the cable and in the return circuits is not zero at all points in the neighborhood of the antenna. By using a high resistance in the secondary circuit the spark discharge becomes aperiodic and radio interference is reduced to a satisfactory level at all wave lengths when used in connection with the harness. The most satisfactory way to introduce the resistance so as to eliminate any possibility of oscillation in the secondary circuit is to use high-resistance ignition cable. This may be accomplished by using a cord wound with high-resistance wire, the pitch of which is usually long as compared to the diameter of the conductor. The wound cord is covered with conducting rubber or other suitable material to eliminate high electrical gradients. The resistance per unit length of the conductor is made so that

$$\frac{R^2}{4L^2} > \frac{1}{CL} \tag{16}$$

which is the condition that the secondary circuit remains aperiodic throughout its length.

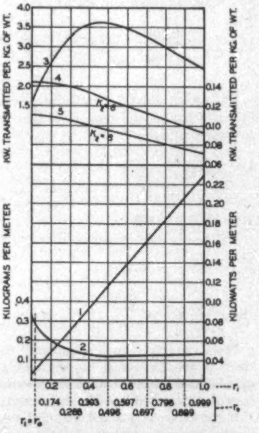


Figure 9. Curves for copper tubes using glass tape impregnated with varnish for insulation

Curve 1—H_0 calculated from equation 10 for 200 amperes
Curve 2—Weight of cable calculated from equation 4
Curve 3—Calculated from equation 6
Curves 4 and 5—Calculated from equation 7

The first completely shielded pressurized ignition system used in this country was installed on the Winnie Mae by Wiley Post. It was manufactured by the Breeze Corporation. The air was taken from the supercharger and fed into the magnetos which were inclosed in airtight boxes to decrease the air leakage. From the magneto the air passed into the manifold and then into the flexible leads to the spark plugs. The air reaching the plugs passed through electrical gradients capable of producing ozone and nitrogen-oxygen compounds. By using hollow conductors air can reach the plugs without ionization, since the gradient in the hollow conductor is zero. As tests made with solid conducting tubing indicated failure from fatigue, flexible conducting tubing is now used.

Experimental Procedure for Low-Voltage Cable

The experimental procedure followed for low-voltage cable was to measure the voltage drop, the temperature of the conductor, and the ambient temperature for a range of current values. From the relation $H_0 = E_{0t}$ the watts generated in the conductor were calculated, and when these values were substituted in equation 3, which is

$$H_0 = 2\pi e(r_1 + t)(T_1 - T)l$$

the value of e for bare conductors over the temperature range $(T_1 - T)$ could be determined, since without insulation $T_1 = T_0$, and T_0 and T were measured.

The thermal conductivity of the insulation of the cables was computed from the temperature difference $T_0 - T_1$ between the conductor and a brass tape wound around and cemented to the insulation with varnish. If the measured value $T_0 - T_1$ is substituted in equation 2,

$$H_0 = \frac{2\pi K(T_0 - T_1)l}{\log \dfrac{r_1 + t}{r_1}}$$

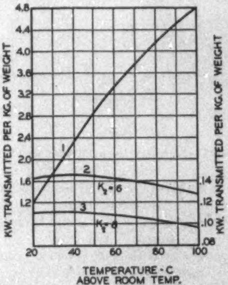


Figure 10. Curves for a copper tube $r_1 = 0.3$, $r_0 = 0.288$ insulated with cotton tape impregnated with varnish

Curve 1 calculated from equation 6
Curves 2 and 3 calculated from equation 7

the thermal conductivity K may be determined.

With the thermal conductivity K determined the temperature T_1 may be obtained from equation 2 when the current, voltage drop per unit length, and the temperature T_0 of the cable are known. If these values are substituted in equation 3, the rate e expressed in watts at which heat is lost per square centimeter per degree centigrade difference in temperature from the surface of the cable may be calculated.

Experimental Procedure for High-Voltage Cable

Three methods were used to determine the effect of ignition cable on the rate of spark-plug electrode wear. The first tests consisted of making measurements upon spark plugs operating for 500 hours at room temperature, the second by photographing the spark discharge in an automotive engine, and the third by testing the rate of wear in an aircraft engine. Photographing the spark discharge is a rapid method of determining electrode wear, since the intensity of the metal lines is proportional to the rate at which the metal evaporates and ionizes in the gap.

Apparatus for Low-Voltage Cable

The method used for measuring the power input versus temperature of the conductor for cable used as open wiring is shown in Figure 1. The sample is four meters long and the distance between the potential leads, $E-E_1$ is three meters. The voltage drop was measured across these leads and the standard resistor I with a potentiometer, from which the power input was calculated. The temperatures were measured at three points by thermocouples indicated by T_0^1, T_0^2, and T_0^3. A fourth thermocouple was used at T_1 when the sample was wrapped with brass tape. The temperature difference $T_0^2-T_1$ was used to determine the thermal conductivity of the insulating material. The ambient temperature was determined by measuring the resistance of a wire stretched parallel to, and about 35 centimeters below the sample. The sample was suspended about 50 centimeters above the table by means of electrodes at the ends and a cord at the center.

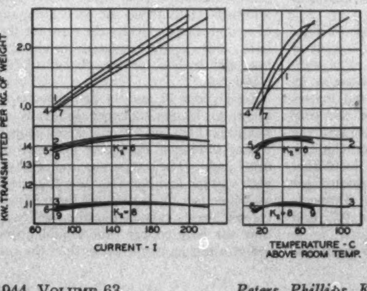
Temperature measurements of conductors inclosed in conduit were made by using the conductor as a resistance thermometer. The resistance R_0 and the temperature coefficient α were determined by measuring the resistance of the conductor at the freezing and boiling points of water. The sample of cable was drawn into the conduit or aluminum tube after 20 feet of the sample had been doubled and potential leads soldered ten feet from the doubled end as shown in Figure 2. The tube was suspended by three silk cords. Measurements of voltage and current were made with a potentiometer. The temperature of the conduit or tube was measured by thermocouples which were imbedded in the walls of the tube and wrapped around the outer surface two or three times to decrease the rate of heat flow from the hot junction. A voltage then was applied to the cable, and the current and potential difference were measured, from which the resistance R and the watts H_0 per unit length could be computed.

Apparatus for High-Voltage Cable

A magneto for a nine-cylinder engine was connected to nine porcelain spark plugs by five-foot lengths of ignition cable inclosed in five-foot lengths of three-eighths-inch copper tubing. Two runs of 500 hours duration were made. The first run was made with ignition cable having a stainless-steel conductor and a capacitance of 22×10^{-12} farads per foot, and the second run was made with cable having a copper conductor and a capacitance of 45×10^{-12} farads per foot. In addition to the resistance of the cable the following resistors were inserted between the spark plug and cable: 0, 100, 500, 1,000, 5,000, 10,000, 25,000, 50,000, and 70,000 ohms.

Photographs of the spark discharge between the electrodes of the spark plugs were made by putting a quartz window in the head of an engine and focusing an image of the spark discharge upon the photographic plate of the spectrograph. The engine was operated with alcohol during the warm-up period so as to reduce the rate at which deposits accumulated on the window. During the photographing of the spark discharge gasoline was used.

Figure 11 Figure 12



Figures 11 and 12. Curves for a 0.498-inch copper tube

Curves 1, 2, and 3 are for the bare copper tube

Curves 4, 5, and 6 are for the tube insulated with cotton tape impregnated with varnish

Curves 7, 8, and 9 are for the tube insulated with glass tape impregnated with varnish

Curves 1, 4, and 7 are computed from equation 6

The rest of the curves were computed from equation 7

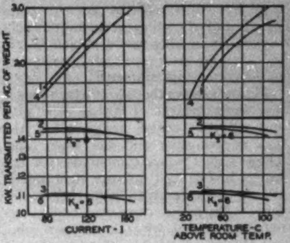


Figure 13 Figure 14

Figures 13 and 14 are curves for a 0.247-inch copper tube

Curves 1, 2, and 3 are for the bare copper tube

Curves 4, 5, and 6 are for the tube insulated with cotton impregnated with varnish

Curves 1 and 4 were computed from equation 6

Curves 2, 3, 5, and 6 were computed from equation 7

Results of Tests for Low-Voltage Cable

The curves in Figure 3 show the number of watts lost for each square centimeter of surface for each degree difference in temperature between the surfaces of the cotton tape with and without varnish impregnation, the 0.5-inch copper tube, the brass wrap, and the ambient temperature. The diameter of the metal and insulated surfaces, together with a description of the insulation and varnish, is given with the curve. This method of giving a description of the curves with the figures is followed throughout. The difference in temperature between the metal conductor and the outer surface of the tape and varnish was calculated from equation 2, after the thermal conductivity of the tape and saturants had been determined by wrapping the cable with brass tape. The thermal conductivity of the cotton tape and three coats of varnish was found to be 0.00045 watt per square centimeter per degree centigrade difference in temperature.

Figure 4 shows the rate at which heat is lost from surfaces of glass tape with and without varnish impregnation. The thermal conductivity of the glass tape and four coats of varnish was found to be 0.0014 watt per square centimeter per degree centigrade difference in temperature. An inspection of the glass tape after the tests showed that the coarse pores were not filled with varnish, and this may explain the smaller rate at which heat is lost from the glass-tape surface compared to the cotton-tape surface.

Figure 5 shows the rate at which heat is lost from surfaces of cotton tape with and without varnish impregnation, the 0.25 copper tube and the brass tape. These values when compared with the values in Figure 4 indicate that heat is lost at a greater rate from the smaller tube. The brass tape was badly corroded compared with the copper tube, and this is indicated by the greater rates at which heat is lost from the brass surface.

Figure 6 shows the rate at which heat is lost from two samples of copper strip. Curve 1 was obtained with the greatest dimension or width of the 0.5-inch strip horizontal, and the other four curves 2, 3, 4, and 5 with the width vertical. Curve 6 was made with the width of the 2.03-inch sample vertical. The samples were supported 50 centimeters above the top of the table as indicated in Figure 1. The thermocouples were put into small holes bored in the strip and then wrapped two or three times around the sample to reduce the flow of heat from the hot junctions of the thermocouples.

Curves 1 and 2 in Figure 7 show the rate at which heat is lost from the surface of two samples of vinylite cable covered with braid and lacquer. Curve 1 is for number-10 conductor consisting of 37 strands, and curve 2 for number-8 conductor consisting of 133 strands. The outside diameter of the number-10 cable was 0.225 inch and of the number-8 cable was 0.255 inch. The surface of the number 10 was smooth, and of the number 8 rough. Curve 3 shows the rate at which heat is lost from the surface of an aluminum-alloy tube when heated by passing current through the tube, and curve 4 when heated by passing current through two number-10 vinylite cables placed in the tube. Curves 5 and 6 were obtained under similar conditions when the aluminum tube was painted with white enamel. Unfortunately the cables were shifted in the tube between the groups of readings shown as curves 3, 4, and 5, 6, so that the temperature difference between the conductor and tubes for the two groups of readings cannot be compared. Curve 7 shows the rate at which heat is lost from the surface of the tube when covered with varnish and carbon black.

In Figure 8 are seven curves which show the difference in temperature between the conductor and the ambient temperature plotted versus current. These values are taken from the same data as used in plotting the curves shown in Figures 3–7 inclusive. Curve 1 shows the difference in temperature between the copper strip and the ambient temperature, and curve 2 shows the difference in temperature between the copper strip when wrapped with cotton tape and the ambient temperature for the values of current plotted as abscissa. The principal reason for the lower temperature of the covered strip is the greater rate at which heat is lost per square centimeter per degree difference in temperature compared to copper. The other five curves are for metal tubes and indicate that for open wiring all bus bars and other conductors should be either insulated or enameled.

In Figure 9 are plotted five curves. Curve 1 is obtained by substituting the following values in equation 10, $i=200$ amperes, $K=0.0014$, $t=0.047$ centimeter, $e=0.002$ watt cm$^2/T_1-T$, $T_0=100$ degrees centigrade, $T=25$ degrees centigrade and $l=100$ centimeters. If the diameter is plotted as abscissa, the curve shows the number of watts which must be dissipated from copper tubular conductors with glass insulation.

Curve 2 is obtained from equation 4 after putting $\delta_1=8.89$ and shows the weight of the cable per meter length for the diameter plotted as abscissa.

Curve 3 is computed from equation 6 and curves 4 and 5 from equation 7 taking K_2 as 6 and 8, respectively. The curves show the weight in kilograms for each kilowatt transmitted through ten meters of cable when the voltage E at the bus bars is taken as 28 volts. No change was made in e as the diameter of the tube decreased in size, as indicated by the values given for 0.5- and 0.25-inch tubing.

Figure 10 shows the number of kilowatts transmitted for each kilogram of weight of the electric system. The voltage E at the bus bars was taken as 28 volts, the tubular conductor ten meters long, the outside diameter of the conductor 0.3 inch = 0.762 centimeter, the thickness of insulation 0.042 centimeter, the thermal conductivity of insulation 0.00045 watt per square centimeter per degree centigrade difference in temperature, the density of insulation 0.7, and a current of 200 amperes.

Each value of e was taken from the average of curves 3, 4, and 5 of Figure 5 by estimating T_1-T and substituting the corresponding value of e in equation 9. If the value obtained by solving the equation for T_1-T did not agree with the estimated value, a second selection was made using the first selection as a guide. Usually the second trial gave the value of e which satisfied equation 9. H_0 then was obtained by substituting these values of e in equation 10. When these data are substituted in equation 6, the kilowatts transmitted per kilogram of weight of cable are shown by curve 1. Substituting the same data in equation 7 after assigning values of 6 and 8 to K_2 respectively, the curves 2 and 3 were obtained. Curve 1 indicates that the higher the temperature the greater will be the number of kilowatts transmitted per kilogram of weight, whereas curves 2 and 3 indicate that for this type and thickness of insulation the temperature of operation of the conductor should be 35 to 40 degrees centigrade above ambient temperature.

Table III. Comparison of Number-8 Cable Weighing 101 Grams Per Meter With Copper Conductor and Cable Weighing 73.6 Grams Per Meter With Aluminum Conductor and Same Resistance

	Current (Amperes)		Kilowatts Transmitted $H-H_0l$		$Wl+K_2H$ Weight of Cable and Power Plant in Kilograms		Kilowatts Transmitted Per Kilogram of Weight From Equation 7		Kilograms for Each Kilowatt Transmitted		
E	Al	Cu	Al	Cu	Al	Cu	Al	Cu	Al	Cu	Dif
30	68.0	79.0	1.93	2.22	12.98	15.23	0.149	0.146	6.72	6.85	0.13
100	70.8	82.8	6.96	8.12	43.22	50.69	0.161	0.160	6.21	6.25	0.04
								Difference	0.53	0.60	
$K_2=6$											
30	59.4	69.0	1.699	1.958	14.992	15.231	0.113	0.111	8.83	8.98	0.15
100	61.7	71.9	6.080	7.068	50.096	50.691	0.121	0.1207	8.24	8.29	0.05
$K_2=8$								Difference	0.59	0.69	

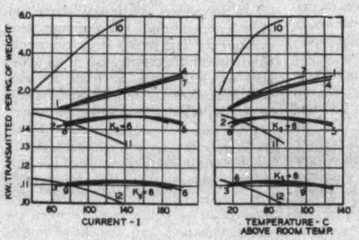
The data used in plotting the curves shown in Figures 3 and 4 were substituted in equations 6 and 7 and the values plotted versus current and temperature as shown in Figures 11 and 12, respectively. The diameter of the copper tube was 0.498 inch. These curves indicate that the copper tubular conductor should operate at a lower temperature when insulated with cotton or glass tape impregnated with varnish with the thicknesses indicated, than when bare. The kilowatts transmitted for each kilogram of weight are practically the same for the bare and cotton-insulated conductor, but slightly less for the glass-insulated conductor.

In Figures 13 and 14 are shown the kilowatts transmitted per kilogram of weight for bare and cotton-covered copper tubing having a conductor diameter of 0.247 inch. The cotton was impregnated with varnish and had the same thickness as the cotton braid used on the 0.5-inch tube. These curves likewise indicate that the bare copper tube should operate at a higher temperature than the insulated tube and that the bare tube transmits a slightly greater number of kilowatts for each kilogram of weight.

In Figures 15 and 16 are shown the kilowatts transmitted per kilogram of weight for two bare copper strips and one cotton-covered strip. The data used in plotting these curves were obtained from the same set of measurements as used in plotting Figure 6. Only the narrow copper strip was covered with tape because of the difficulty of holding the insulation in contact with the wide metal strip.

Using the aluminum-alloy tube as a conductor, the kilowatts transmitted per kilogram of weight are shown in plots 17 and 18 for the bare and enameled tubes.

The plots shown in Figures 19 and 20 indicate the number of kilowatts transmitted for each kilogram of weight of the electric system. Plots 1, 2, and 3 were for number-10 conductor insulated with vinylite, braid, and lacquer. Plots 4, 5, and 6 were for number-8 conductor insulated with vinylite, braid, and lacquer, and plots 7, 8, and 9 were number-8 conductor insulated with glass tape impregnated with lacquer. The data used here were obtained from the same set of measurements used in plotting Figure 7.



Figures 15 (left) and 16 (right). Curves for copper strips

Curves 1, 2, and 3—Bare copper strip, 0.5x0.095-inch width horizontal

Curves 4, 5, and 6—Bare copper strip, 0.5x0.095-inch width vertical

Curves 7, 8, and 9—Copper strip insulated with braid and impregnated with varnish

Curves 10, 11, and 12—Bare copper strip, 2.03x0.003-inch width vertical

Curves 1, 4, 7, and 10 were computed from equation 6. The remaining curves were computed from equation 7

Cable in Conduit

Only two sets of observations were made with cable in conduit, one using two number-10 cables covered with vinylite insulation, braid, and lacquer, the other using two number-8 cables insulated with varnish-impregnated glass tape.

The results for the samples of vinylite are shown in Figures 21 and 22 and indicate that the maximum power is delivered per unit of weight when the two cables carry between 80 and 90 amperes. These measurements apply only to the use of two cables in 0.5-inch conduit. The optimum temperature of operation of the conductor is between 50 and 60 degrees centigrade above ambient temperature for the bare tube. Painting the conduit with white enamel slightly increases the number of kilowatts transmitted for each kilogram of weight and lowers the temperature of operation of the conductor five to ten degrees centigrade.

Comparison of the results for the samples of glass-impregnated cable shown in Figures 23 and 24 with the curves shown in Figures 21 and 22 indicates that the use of glass braid and varnish for an insulating and protective coating increases the kilowatts which may be transmitted for each kilogram of weight. Painting the tube with varnish and carbon black lowers the temperature of the conductor.

In Figure 25 is shown the drop in temperature between the conductor and conduit, the conductor and ambient temperature, and the conduit and ambient temperature. Curves 1 to 3 are for the number-10 conductor insulated with vinylite, braid, and lacquer in the bare conduit, and curves 4 to 6 are values for the same cable in the same conduit painted with white enamel. Curves 7 to 9 are for the number-8 conductor insulated with glass tape impregnated with varnish in the bare conduit, and curves 10 to 12 are for the same cable in the same conduit covered with carbon black and varnish.

Altitude

No measurements have been made at reduced pressures to simulate operation at high altitudes. From the little data available it seems that a tentative solution is to assume the ambient temperature has been raised 35 degrees centigrade. For example, if cable operates satisfactorily at 25 degrees centigrade at sea level it should operate equally well at high altitudes and temperatures ten degrees centigrade below zero. The limiting value occurs when the air becomes sufficiently rare to permit cooling only by radiation.

Voltage

The same data were used for computing the curves shown in Figure 26 as were used in plotting the curves shown in Figures 19 and 20. The weight of the number-8 vinylite-insulated cable was 101.1 grams per meter. The loss in watts when conducting a current of 60.7 amperes was 8.7 watts per meter. From these data the resistance was taken as 0.00236 ohm. Curves 1 and 2 show the number of kilowatts transmitted for each kilogram of weight for voltages 10 to 100 using a cable ten meters long and assigning K_2 the values 6 and 8, respectively. The values were obtained from equation 7 after substituting in the values for optimum current obtained from equation 8. Curves 3 and 4 show the optimum current for the voltages plotted as abscissa after assigning K_2 the values 6 and 8, respectively. To simplify the calculations the resistance was assumed constant, and the correction $R=R_0(1+\alpha T_0)$ was not applied, although sufficient data have been given to make the correction.

Tests on Insulation

These data have been given in such a way that the temperature requirements for the insulation can be determined when the ambient temperatures are known. Information regarding the highest temperature at which an insulation can operate without rapid deterioration may be obtained from the manufacturers of the better insulating materials. During this investigation insulations were found which melted and oozed through the protective coating sufficiently to cause the cables to stick together, thus making it impossible to remove a damaged cable if a group of cables were inclosed in a conduit.

The number-8 conductor insulated with glass tape and impregnated with varnish seems to have possibilities. It was operated at 230 degrees centigrade without apparent damage. It was soaked in salt water with the ends immersed for 24 hours and then withstood 1,500 volts alternating current with the ends protruding $1^1/_2$ inches above the surface of the water. After bending around a one-inch mandrel at −50 degrees centigrade it withstood the same voltage test.

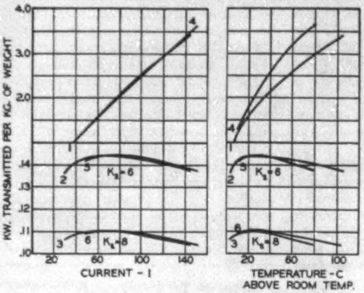
Results of Tests for High-Voltage Ignition Cable

The spark-plug wear of 18 spark plugs after 500 hours operation at room temperature with resistors between the spark plugs and cables is given in Table II. The magneto was operated at 900 rpm and the gaps set at 0.025 inch.

In Figure 27 are shown nine illustrations of the spark discharge together with the description of each discharge.

Discussion of Results

Figures 3 to 7, inclusive, show that the rate at which heat is lost per square centimeter of surface per degree centigrade difference in temperature between the surface of the cable and ambient temperature is less for metallic than for nonmetallic surfaces. The surfaces of the copper samples, the brass tape, and the



Figures 17 (left) and 18 (right). Curves for aluminum tubing

Curves 1, 2, and 3—Bare aluminum tubing

Curves 4, 5, and 6—Enameled covered aluminum tubing

Curves 1 and 4 were computed from equation 6

Curves 2, 3, 5, and 6 were computed from equation 7

aluminum-alloy tube were oxidized, which probably represents an average condition for such metals in service.

The glass tape referred to in Figure 4 was only partly filled with varnish. This may account for the smaller rate at which heat was lost from the surface as compared to the cotton tape shown in Figure 3. The rate at which heat was lost from the surface of the number-8 cable with glass braid is not shown, because there is some doubt as to the value obtained for the thermal conductivity of the insulation. The uncertainty in the value was due to the thinness of the insulation and stranding of the conductor. The temperature T_1 could not be calculated without knowing the thermal conductivity K. It was not necessary to know the temperature T_1 to plot the curves for this cable shown by Figures 19 and 20. The addition of carbon black to the varnish did not greatly increase the rate at which heat was lost from the surface of the cable, which is in accord with accepted theory.

In Figure 8 are shown the changes in temperature when the metal surfaces are covered with braid and varnish. For example, covering a copper strip with thin braid and varnish decreased the temperature from 129 to 93 degrees centigrade, covering an alloy of aluminum with white enamel lowered the temperature from 99 to 70 degrees centigrade, and covering the same tube with carbon black and varnish reduced the temperature to 63 degrees centigrade. These results suggest coating all bus bars and conduit with white enamel or preferably varnish and carbon black.

In Figure 9 the inner and outer radii of the metal tubes r_1 and r_0 are plotted as abscissa, and the difference between the two indicates the thickness of the wall. In curve 2 are values plotted from equation 4, indicating that for the values used in these computations a radius of 0.5 inch will give the minimum weight. Curve 3 which is the values obtained from equation 6 shows that there is a diameter and wall thickness which permit a substantial saving in weight, whereas curves 4 and 5 which are values obtained from equation 7 indicate that if the weight of the power plant is added to the weight of the cable a solid conductor should be used. A change in the values substituted in the two equations may alter these conclusions.

The three curves shown in Figure 10 agree with the curves plotted from experimental data. These data are shown in Figures 11 to 20, inclusive. The curves indicate that if the weight of the power plant is neglected copper conductors operating at 100 degrees centigrade can transmit from 1.0 to 4.0 kw at 28 volts by a conductor ten meters long. The one exception is the 2.03-inch strip of bare copper which will transmit about 6.3 kw per kilogram of weight. The optimum current from equation 6 when the weight of the power plant is neglected may be written

$$i=\frac{E}{2Rl}=\frac{E}{2lR_0(1+\alpha T_0)}$$

If this value of i is substituted in equation 6,

$$H_z=\frac{E^2}{4R_0l^2W(1+\alpha T_0)}=\frac{1}{\delta\rho(1+\alpha T_0)}\left(\frac{E}{2l^2}\right)^2$$

which shows that the density δ and resistivity ρ of the metal determine the maximum value H_z may take for a voltage E applied to a circuit of length l, and indicate the importance of increasing the voltage when the length of the circuit is increased.

If means are provided for maintaining the temperature of this strip at 101.7 degrees centigrade either by forcing air over the surface or increasing the radiating surface without changing the cross-sectional area the resistance will remain 0.00386 ohm. The optimum current for the strip taking the length $l=10$ meters and the voltage $E=28$ volts will be $i=362.4$, and the maximum number of kilowatts which may be transmitted per kilogram of weight is 9.6.

If an allowance of six grams is made for each watt dissipated as heat the operating temperature of the bare copper tubes and narrow strip should be 50 to 65 degrees centigrade above ambient temperature, the insulated tubes 40 to 50 degrees centigrade above ambient temperature, the aluminum-alloy tube 25 to 50 degrees centigrade above ambient temperature, and the copper conductor insulated with braid and varnish 30 to 40 degrees centigrade above ambient temperature. The optimum temperature of the broad strip cannot be determined from the curves. If an allowance of eight grams is made for each watt dissipated the temperatures should be five to ten degrees centigrade lower.

If the broad copper strip were insulated its increase in weight would decrease the number of kilowatts transmitted per kilogram of weight and increase the operating temperature. For insulated strips there is an optimum ratio between the width, thickness, and amount of insulation.

In Figures 21 and 22 are shown the results of testing the two samples of number 10 conductor insulated with vinylite, braid, and lacquer in an aluminum tube or conduit, and in Figures 23 and 24 are shown similar curves for two samples of number-8 conductor insulated with glass braid and lacquer in conduit.

If these values are compared with those in Figures 19 and 20, the increase in weight of the electric system after

Figures 19 (left) and 20 (right). Cable with stranded conductors

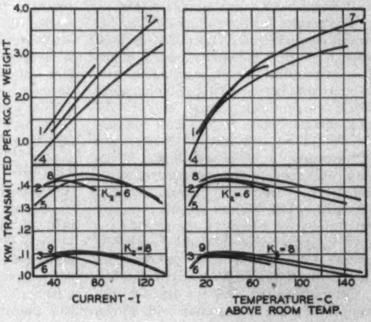
Curves 1, 2, and 3—Number-10 stranded conductor covered with vinylite cotton braid and lacquer

Curves 4, 5, and 6—Number-8 stranded conductor covered with vinylite cotton braid and lacquer

Curves 7, 8, and 9—Number-8 stranded conductor covered with glass braid impregnated with varnish

Curves 1, 4, and 7 were computed from equation 6

Curves 2, 3, 5, 6, 8, and 9 were computed from equation 7



inclosing two number-10 cables in the aluminum conduit was 30 per cent for $K_2=6$ and 24 per cent for $K_2=8$. Inclosing the two number-8 cables insulated with glass tape and varnish in the conduit increased the weight of the electric system five per cent for $K_2=6$, and four per cent for $K_2=8$.

Although these values may seem a little misleading, the two number-10 cables fit loosely in the conduit, while the two number-8 cables were tightly squeezed against the inner wall. If we refer to Figure 25 it will be observed that the drop in temperature between the conductor and the conduit is less for the number-8 than for the number-10 cable, and this greater rate of removing heat from the cable is an important factor for increasing the number of kilowatts transmitted by the electric system per kilogram of weight. An inspection of Figures 21 to 24 shows that the range of temperature for the cables in conduit is from 45 to 80 degrees centigrade. This is higher than for open wiring.

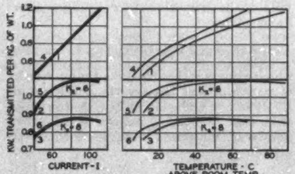
Aluminum of sufficient purity for electric conductors was not available to the authors at the time measurements were made, but a general idea may be obtained as to the possible saving in weight by the substitution of the constants for aluminum in place of copper in equations 6 and 7.

The weight of the copper conductor $W_c=A_c\delta_c l$ where A_c is the cross-sectional area, δ_c the density of the copper, and l the length. The resistance $R_c=\rho_c l/A_c$ where ρ_c is the resistivity so that $W_c=\rho_c\delta_c l^2/R_c$.

In a similar manner the weight of the aluminum conductor may be written $W_a=\rho_a\delta_a l^2/R_a$

If the same voltage drop is permitted for i amperes,

$$R_c=R_a \text{ and } W_a=W_c\frac{\rho_a\delta_a}{\rho_c\delta_c}$$



Figures 21 (left) and 22 (right). Two cables in aluminum conduit

Curves 1, 2, and 3—Number-10 stranded conductor covered with vinylite cotton braid and lacquer in an aluminum conduit

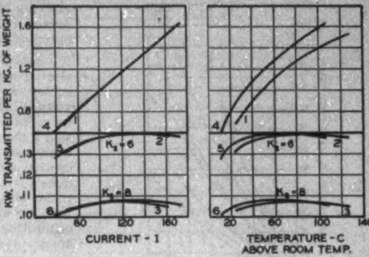
Curves 4, 5, and 6—Same cable in the same aluminum conduit after painting with white enamel

Curves 1 and 4 were computed from equation 6

Curves 2, 3, 5, and 6 were computed from equation 7

Weight of cable 70 grams per meter

Weight of conduit 101.7 grams per meter



Figures 23 (left) and 24 (right). Curves for two cables in aluminum conduit

Curves 1, 2, and 3—Number-8 stranded conductor covered with glass tape impregnated with varnish in an aluminum conduit

Curves 4, 5, and 6—Number-8 stranded conductor covered with glass tape impregnated with varnish in an aluminum conduit after painting with carbon black in varnish

Curves 1 and 4 were computed from equation 6

Curves 2, 3, 5, and 6 were computed from equation 7

Weight of cable 83.5 grams per meter

Weight of conduit 101.7 grams per meter

If W_a is substituted for W_c in equation 6, the kilowatts transmitted for each kilogram of weight are

$$H_a=\frac{\frac{H}{l}-H_0}{W_c\frac{\rho_a\delta_a}{\rho_c\delta_c}}=\frac{\rho_a\delta_a}{\rho_c\delta_c}\left(\frac{\frac{H}{l}-H_0}{W_c}\right)=2H_c$$

which indicates with bare conductors a saving of 50 per cent in weight. This neglects the greater area of the conductor for dissipating heat as well as the lower rate at which heat is lost from the aluminum surface compared to the copper surface. If neither surface is painted the losses from the two conductors should be nearly the same. If aluminum is substituted for copper in the number-8 conductor insulated with vinylite, braid, and lacquer the weight of the conductor would be 71×0.5=35.5 grams, since the weight of the copper conductor is 71 grams per meter. The diameter of the conductors and consequently their circumference will be in the ratio of the square root of their resistivities, or

$$\frac{r_a}{r_c}=\sqrt{\frac{\rho_a}{\rho_c}}=1.27$$

The weight of insulation per meter of the number-8 vinylite cable is 30 grams, so that the insulation with the aluminum conductors will weigh 30×1.27=38.1 grams for the same thickness of insulation. If these values are substituted in equation 6, the kilowatts transmitted per kilogram of weight for aluminum compared to copper is

$$H_a=\frac{101}{73.6}H_c=1.37H_c$$

When the weight of the power plant is added to the weight of the cables it is necessary in comparing aluminum and copper conductors to use the optimum current for each cable. This current may be computed from equation 8 after the length of circuit, weight of cable, resistance, K_2, and voltage of operation are known. In Table III are given values which permit a comparison of the number-8 cable weighing 101 grams per meter and having a copper conductor with the cable weighing 73.6 grams per meter and having an aluminum conductor and the same resistance. To simplify the calculations, it is assumed that the resistance of the copper and aluminum conductors remains at 0.00236 ohm per meter. In the second column of the table is given the optimum current for the voltages in column 1. In column 6 is given the weight in kilograms for each kilowatt transmitted by a cable ten meters long. At 30 volts the saving in weight by replacing copper with aluminum is 0.13 kilogram for $K_2=6$ and 0.15 kilogram for $K_2=8$, for each kilowatt transmitted. If the voltage is increased from 30 to 100 the saving in weight is from 0.5 to 0.7 kilogram for values of $K_2=6$ and $K_2=8$, respectively, for each kilowatt delivered, so that, if a substantial saving in weight is desired for cables which operate continuously, an increase in voltage is necessary. Figure 26 shows that the voltage for this installation which consists of a single conductor and power plant should not be less than 50 or 60 volts.

In practice the optimum current capacity of a cable may be determined experimentally with the arrangement of apparatus as shown in Figure 1. A table may then be compiled or a curve drawn showing the number of watts transmitted for each kilogram of weight with a circuit length l, voltage E, and current I. The measurements can be made with open wiring or the cable enclosed in conduit. If this procedure is used the increase in resistance with temperature becomes a factor in determining the current capacity of the cable. From these data the cable may be selected.

For open wiring aluminum strips or bars offer many advantages. The cooling surface of strips may be large compared to the circular conductor. Terminals may be eliminated at junction boxes and connections made by bolting the metal strips together.

The three parts designated 3, 4, and 5 in Figure 27, show that the intensity of

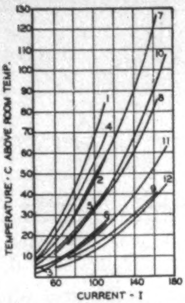


Figure 25. Curves showing the drop in temperature between the conductor and ambient temperature for two types of cable

Curves 1, 2, and 3 show T_0-T, T_1-T and T_0-T_1 for the sample of vinylite-insulated number-10 cable in an aluminum conduit

Curves 4, 5, and 6 show the corresponding values for difference in temperature when the tube is covered with white enamel

Curves 7, 8, and 9 show T_0-T, T_1-T, and T_0-T_1 for number-8 cable insulated with glass braid in an aluminum tube

Curves 10, 11, and 12 show the corresponding values for difference in temperature when the tube is covered with carbon black in varnish

the metal lines in the spectra of the spark discharge increases with an increase of capacitance in the secondary circuit. This is in agreement with the well-known fact that an increase in capacitance increases the rate of spark-plug wear. Parts 6 and 8 indicate that the metal lines have practically the saeme intensity with 100 ohms in the circuit as with 10,000 ohms, and this agrees with the results shown in Table II. Photographing the spectra of the spark discharge offers a quick method of studying spark-plug wear. Recent tests made on aircraft engines indicate the wear may be decreased 50 per cent by using high-resistance cable.

Conclusions

1. In practice the current-carrying capacity and temperature of operation of a cable may be determined by measuring the temperature and voltage drop for a range of current values. These data are substituted in equations 6 and 7, after selecting the voltage E at the bus bar and allowing a value for K_2 which meets the conditions of the services. It may be six or eight grams as used throughout these calculations, or it may be radically different if an auxiliary power plant and storage battery are used. When these values are plotted, the greatest number of kilowatts transmitted per kilogram of weight and the temperature of operation may be selected as indicated in Figures 11 to 20, inclusive. The value for current-carrying capacity and temperature of conductor will be different when the cable is operated in conduit or as open wiring.

2. Substantial reduction in weight of the electric system for continuously operated cables can be obtained by increasing the voltage. Substituting aluminum conductors for copper reduces the weight of the installation if the weight of the power-plant unit is neglected. For continuous operation in conduit a small change in voltage will produce a greater saving in weight than substituting aluminum conductors for copper. If bars are used, the troubles experienced in connecting the aluminum conductors to the apparatus or together are greatly simplified, since holes may be bored in the strips and two or more conductors bolted together.

3. Conduit and bus bars should be painted, because the temperature of operation is lower when there is nonmetallic material between the metallic surface and the surrounding atmosphere.

Appendix A. Analysis of Peak-Load Supply

The weight of a 24-volt battery in terms of output in watts for four rates of discharge is shown in Figure 28.* The relation between battery weight in pounds, and power (H_x) in kilowatts, may be expressed sufficiently closely for all practical purposes by the equation,

$$W_B=K_0+(K_1+K_2t+K_3t^2+K_4t^3)H_x \qquad (17)$$

where the constant $K_0=20$ is the intercept of the curve with $H_x=0$ and the constants $K_1=12.43$, $K_2=4.767$, $K_3=-0.32$, $K_4=0.01$ are obtained by solving the equation with the values of H_x and t taken for four rates of discharge. The minute rate of battery discharge is t.

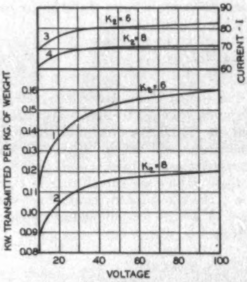


Figure 26. Curves showing the optimum current and the number of kilowatts transmitted per kilogram of weight for a range of voltages from 0 to 100

Curves 1 and 2 were computed from equation 7

Curves 3 and 4 were computed from equation 8

The weight of the generator may be written

$$W_G=K_1'+K_2'H_g \qquad (18)$$

The weight of the auxiliary engine or generator drive from the main engine may be written in terms of output

$$W_E=K_1''+K_2''H_g \qquad (19)$$

If the weight of the generator and power supply are added together the sum of the two may be designated by the equation,

$$W_{GE}=K_1'''+K_2'''H_g \qquad (20)$$

In Figure 29 is shown a portion of the load chart where the greatest output H required of the combination of battery and generator is represented as occurring between the times t_n and t_{n+1}.

The battery must supply H kilowatts for the time $(t_{n+1}-t_n)$, and the generator must recharge the battery during the time $t_{n+2}-t_{n+1}$. Since the battery is not 100 per cent efficient, additional energy must be supplied by the generator. The energy supplied by the generator to charge the battery during the time $t_{n+2}-t_{n+1}$ after the latter supplied H_x kw for $(t_{n+1}-t_n)$ minutes may be written

$$H_x\frac{(t_{n+1}-t_n)\alpha}{t_{n+2}-t_{n+1}}=H_xB \qquad (21)$$

where $\alpha>1$ and designates the ratio between input and output of the battery. The generator must supply H_1+H_xB kw and since

$$H=H_1+H_xB+H_x$$

$$H_1+H_xB=H-H_x \qquad (22)$$

Equation 20 may be written

$$W_{GE}=K_1'''+K_2'''(H-H_x) \qquad (23)$$

If equations 17 and 23 are added, the total weight of the generator and battery may be written

$$W=K_0+K_1'''+(K_1+K_2t+K_3t^2+K_4t^3)\times H_x+K_2'''(H-H_x) \qquad (24)$$

Putting the derivative of this equation equal 0 and solving

$$K_2'''=(K_1+K_2t+K_3t^2+K_4t^3) \qquad (25)$$

which expresses that for an optimum weight of generator and battery the coefficient of H_x should be the same for both battery and generator. If the generator is used to supply all the power, equation 20 may be written

$$W_{GE}=K_1'''+K_2'''H \qquad (26)$$

Subtracting equation 26 from equation 24 gives the difference between the weight of a generator–battery unit and that of a generator capable of supplying the same peak load. The difference is

$$\Delta W=K_0+(K_1-K_2'''+K_2t+K_3t^2+K_4t^3)H^x \qquad (27)$$

If W is positive a generator should supply all the power and if W is negative a battery should be used with the generator.

With the data available to the authors K_1''' is not known. By putting it equal to zero $K_2'''=66/5=13.2$ pounds per kilowatt, which is the maximum value this constant can take assuming 66 pounds as the weight of generator and drive shaft for a five-kilowatt unit. When this value is substituted in equation 27 with the other constants,

---

* These curves were supplied by Carlos B. Mirick. More recent data on storage batteries indicating greater output per pound have not been released for publication.

$$\Delta W=20+(12.43-13.2+4.767t-0.32t^2+0.01t^3)H_x$$

and for a slightly smaller value of K''' which would be obtained by making $K_1'''>0$, the coefficient of H_x would be zero for all values of t within the range for which t and H_x are shown in Figure 29.

For a surge, where t may for all practical purposes be taken as zero $\Delta W=0$ when $H_x=20/0.77=26$ kw and a battery may be used when $H_x>26$ kw. For $t>0.15$ minute rate, ΔW remains, positive regardless of the value assigned to H_x.

No allowance is made for fuel, and this favors the battery, since for each gram of fuel consumed by the power plant only $1/\alpha$ gram is returned by the battery as useful work. As the voltage is increased the weight of the battery is increased so that the difference in weight ΔW is increased.

A suggestion often has been made that the use of a battery may be justified for peak loads if it is charged at 28 volts and connected in series with the generator to supply that portion of the airplane equipment which causes overloading of the generator. This would permit a portion of the equipment to operate at $2E$ volts.

Appendix B. Derivation of Equation for Selection of Tubular Conductors

Figure 30 represents the cross section of a hollow conductor cable. The inner radius of the conductor is designated by r_0, the outer radius by r_1, and the radius of the cable by r_1+t, where t is the thickness of the insulation. The rate of heat generation by the conductor is given by the equation,

$$H_0=\frac{i^2\rho l(1+\alpha T_0)}{\pi(r_1^2-r_0^2)}=E_0 i \tag{28}$$

where H_0 is the heat in watts, T_0 the temperature of the conductor, $\pi(r_1^2-r_0^2)$ the cross-sectional area of the conductor, l the length of the cable, ρ the resistivity, and α the temperature coefficient of resistivity of the conductor.

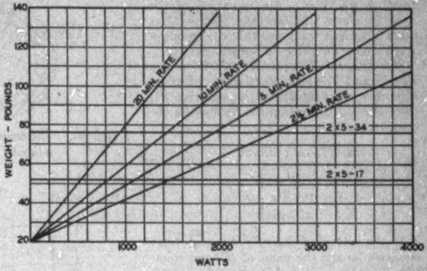


Figure 28. The weight in pounds of a 24-volt aviation battery versus watts output for four rates of discharge

The rate at which heat is conducted through the insulation is given by the equation,

$$H_0=\frac{2\pi Kl(T_0-T_1)}{\log\frac{r_1+t}{r_1}} \tag{29}$$

where T_1 is the temperature of the surface of the cable and K is the thermal conductivity of the insulation. The temperatures T_0 and T_1 will increase until the heat lost from the outer surface of the insulation is equal to the rate at which heat is generated by the conductor. This may be represented by the equation,

$$H_0=2\pi el(r_1+t)(T_1-T) \tag{30}$$

where T is the ambient temperature and e is the rate at which heat is lost in watts from each square centimeter of surface for each degree centigrade difference in temperature.

The difference in temperature between the conductor and ambient is T_0-T, and this may be written as the sum of the differences of the temperature between the conductor and the outer surface, and the outer surface and the ambient temperature, that is,

$$(T_0-T_1)+(T_1-T)=(T_0-T) \tag{31}$$

The weight of the cable may be written as

$$W_c=\pi(r_1^2-r_0^2)l\delta_1+\pi(2r_1+t)\delta_2 l \tag{32}$$

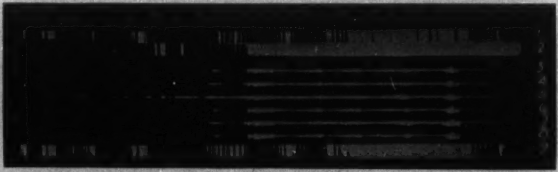
where δ_1 is the density of the conductor, and δ_2 is the density of the insulation.

Figure 27. The spectrum of the spark discharge across the electrodes of a spark plug during the operation of an automotive engine

1. Iron arc
2. Standard r.v. powder

Photographed for identification of metal lines

3. 50×10^{-12} farad secondary capacitance
4. 150×10^{-12} farad secondary capacitance
5. 250×10^{-12} farad secondary capacitance
6. 100 ohms between ignition cable and spark plugs with 50×10^{-12} farad secondary capacitance
7. 150×10^{-12} farad secondary capacitance
8. 10,000 ohms between ignition cable and spark plugs with 50×10^{-12} farad secondary capacitance
9. Iron arc, same as number 1



If equations 28, 29, and 30 are substituted in equation 31, and we solve for the cross-sectional area,

$$\pi(r_1^2-r_0^2)=\frac{1}{2\pi B}\left\{\frac{\log\frac{r_1+t}{r_1}}{K}+\frac{1}{e(r_1+t)}\right\} \tag{33}$$

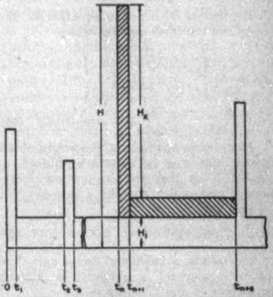


Figure 29. A portion of a load chart where *H* is the greatest output of the generator and battery and *H*$_x$ is the output of the battery

where

$$B=\frac{T_0-T}{i^2\rho(1+\alpha T_0)}$$

An inspection of this equation shows that if the outside diameter of the cable is (r_1+t), the thermal conductivity of the insulation is K, and e is the rate at which heat is lost per square centimeter per degree centigrade difference in temperature from the surface of the cable, then for a given current i and conductor temperature T_0, the cross-sectional area of the conductor is defined with r_0 serving as the only variable. After $r_0=0$ the equation continues to give values for the cross-sectional area as r_1 is decreased, but an inspection of the values indicates that the cross-sectional area continues to increase as r_1 is decreased. This follows since the rate of loss of heat is a function of the radius r_1, and the solution of the equation gives the cross-sectional area required to dissipate the heat i^2R from the surface of the cable which is $2\pi el(r_1+t)\times(T_1-T)$. Since r_1 is decreasing R must decrease, and this can take place only by increasing the area. An inspection of the equation shows that this takes place when r_0 takes imaginary values so that the equation applies only for values of r_1 making $r_0 \geqq 0$.

If equations 29 and 30 are solved for $T_0 - T_1$ and $T_1 - T$, respectively, and these equations are substituted in equation 31, the following expression for H_0 may be obtained:

$$H_0 = \frac{2\pi l(T_0 - T)}{\left\{\frac{\log \frac{r_1+t}{r_1}}{K} + \frac{1}{e(r_1+t)}\right\}} \tag{34}$$

If the value of $T_0 - T_1$ obtained from equation 29 and the value of H_0 from equation 34 are substituted in equation 31, the following equation may be written for $T_1 - T$:

$$T_1 - T = (T_0 - T)\left\{1 - \frac{\log \frac{r_1+t}{r_1}}{\log \frac{r_1+t}{r_1} + \frac{K}{e(r_1+t)}}\right\} \tag{35}$$

If equation 33 is put in equation 32, the weight of the cable may be written

$$W_c = \frac{1}{2\pi B}\left\{\frac{\log \frac{r_1+t}{r_1}}{K} + \frac{1}{e(r_1+t)}\right\} \times l\delta_1 + \pi(2r_1+t)t\delta_2 l \tag{36}$$

As stated in Appendix *A*, the weight of the power unit may be written as $W_G = K_2 \times (E+E_0)I$, so that for the portion of the power unit which supplies i amperes the weight may be written

$$W_G = K_2(E+E_0)i \tag{37}$$

The weight of cable and power unit supplying i amperes is obtained by adding equations 36 and 37 which is

$$W_c = \frac{1}{2\pi B}\left\{\frac{\log \frac{r_1+t}{r_1}}{K} + \frac{1}{e(r_1+t)}\right\} \times l\delta_1 + \pi(2r_1+t)t\delta_2 l + K_2(E+E_0)i \tag{38}$$

If equation 28 is solved for $\pi(r_1^2 - r_0^2)$, the relation may be written

$$\pi(r_1^2 - r_0^2) = \frac{i\rho(1 \div \alpha T_0)}{E_0} l$$

and

$$r_1 = \sqrt{r_0^2 - \frac{i\rho(1+\alpha T_0)l}{\pi E_0}}$$

If these are substituted in equation 32, the weight of the unit supplying i amperes may be written

$$W_c = \frac{i\rho(1+\alpha T_0)}{E_0} l\delta_1 + \pi\left(2\sqrt{r_0^2 - \frac{i\rho(1+\alpha T_0)}{\pi E_0}} + t\right) \times lt\delta_2 + K_2(E+E_0)i \tag{39}$$

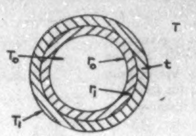


Figure 30. The cross section of a cable with a tubular conductor

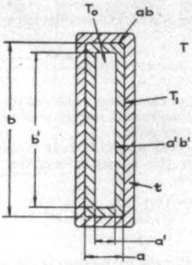


Figure 31. The cross section of an insulated hollow rectangular conductor

Taking the derivative of equation 39 with respect to E_0 and putting it equal to 0 gives the optimum value for the drop in voltage versus weight. Less is learned by following this procedure than by plotting equation 38 for a few values of r_1 and determining $H_0 = E_0 i_0$ from equation 34.

For bare conductors $t = 0$ and the second term of equation 39 becomes 0. If the derivative of W_c is taken with respect to E_0 and set equal to 0, the drop in volts for bare conductors may be written

$$E_0 = l\sqrt{\frac{\rho(1+\alpha T_0)}{K_2}\delta_1} \tag{40}$$

This equation indicates that for each circuit of length l the drop in voltage should be a constant multiplied by l. The following examination shows that an average voltage drop may be obtained throughout the distribution system which does not change the total weight of wire.

Let W_1, W_2, W_3, etc., be the weights having voltage drops of E_1, E_2, E_3, etc., respectively. Then $W_1E_1 + W_2E_2 \ldots W_nE_n = WE'$ where W is the total weight of cable.
If we solve for E'

$$E' = \frac{W_1E_1 + W_2E_2 + \ldots W_nE_n}{W} = \frac{\Sigma_1^n W_\alpha E_\alpha}{W}$$

and the new weights may be written

$$W_1' = \frac{W_1E_1}{E'},\quad W_2^1 = \frac{W_2E_2}{E'} \ldots W_n' = \frac{W_nE_n}{E'} \tag{41}$$

or

$$E'\Sigma_1^n W_\alpha' = \Sigma_1^n W_\alpha E_\alpha = E'W$$

so

$$W = \Sigma_1^n W_\alpha'$$

which shows there is no change in weight.

There is a small change in the weight of the generator because of the change of the voltage drop in the line, but this change is small compared to the change which may be caused by the conductor.

Appendix C. Derivation of Equation for Selection of Rectangular Conductors

The rate of heat generation by the current I in a rectangular hollow conductor as represented in Figure 30 may be written

$$H_0 = \frac{i^2\rho(1+\alpha T_0)}{ab - a_1b_1} l \tag{42}$$

where $ab - a_1b_1$ is the cross-sectional area of the strip, and the other letters designate the same quantities as given in Appendix *C*.

The rate at which this quantity of heat passes through the insulation is expressed by the equation,

$$H_0 = \frac{2K}{t}(a+b+2t)(T_0 - T)l \tag{43}$$

and this must equal the rate at which heat is lost from the surface and is expressed by the equation,

$$H_0 = 2e(a+b+4t)(T_1 - T)l \tag{44}$$

The relation between the temperature of the conductor and the ambient temperature may be written

$$(T_0 - T_1) + (T_1 - T) = T_0 - T \tag{45}$$

The weight of insulated strip W_S is expressed by the equation,

$$W_S = (ab - a_1b_1)l\delta_1 + 2(a+b+2t)lt\delta_2 \tag{46}$$

When $a_1b_1 = 0$, the same limitations are imposed as for the tubular conductor. If equations 42, 43, 44, and 45 are substituted in equation 46, the weight of the insulated strip may be written

$$W_c = \frac{1}{B_1}\left\{\frac{t}{2K(a+b+2t)} + \frac{1}{2e(a+b+4t)}\right\} l\delta_1 + 2t(a+b+2t)l\delta_2 \tag{47}$$

where

$$B_1 = \frac{T_0 - T}{i^2\rho(1+\alpha T_0)}$$

Reference

1. Optimum Voltage for Airplanes, Vernon H. Grant, Melville F. Peters. Electrical Engineering, volume 58, October 1939, pages 428–31.

Aircraft-Electric-Accessory-Vibration Investigations

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Synopsis: Flight and laboratory tests are in progress to determine changes in design of aircraft generators which will improve their resistance to vibration-fatigue failure. Vibrations near mountings of generator voltage regulators and reverse-current relays are being measured to provide data for design and testing of these accessories.

The paper presents flight vibration data and discusses sources of vibration in aircraft, accessory-vibration characteristics, vibration test apparatus, and investigations of failures of generator mountings and flexible drive asemblies.

On the basis of the flight and laboratory tests and other available information, recommendations are made for the design of generators which will give most satisfactory mechanical performance. The primary requirements of a generator from the standpoint of avoiding vibration failures are a stiff mounting flange and a flexible drive assembly having a satisfactory damping device.

FAILURE of a part of the electric system of a combat aircraft can be responsible for the loss of the aircraft and flight personnel because of fires and reduced fighting capacity. The most prevalent failures in the electric system are caused by vibration of main engine-driven generators.

Designers of electric accessories for aircraft have been handicapped by lack of knowledge of vibration conditions encountered, and performance of accessories under operating conditions and by inadequate testing facilities. Translational vibration test specifications for other than engine-mounted accessories give some knowledge of conditions to be expected, but the specifications have been adjusted to meet the limitations of generally available test apparatus. The ability of generators to withstand engine vibrations has been determined by actual flight performance without measurement of vibration. This procedure has resulted in some designs which had inadequate factors of safety, and large numbers of units have been produced before failures were encountered.

To obtain flight vibration data on aircraft accessories for improvements in existing designs and for better new designs, a program of correlated flight and laboratory tests is being conducted by the author's company in co-operation with the Army Air Forces. Initial flight tests were made in co-operation with the Westinghouse Electric and Manufacturing Company on a *B*-17 flying fortress. These tests consisted of measurements of vibration on main engine-driven generators and adjacent to generator voltage regulators and reverse-current relays. Following the *B*-17 studies, the failure of generator mountings on the *B*-24 liberator bomber was investigated; the causes of failure were determined; and a service correction and a design change were made to prevent further failures. Laboratory and flight investigations of torsional vibrations on aircraft generators are now in progress. On the basis of these investigations some design changes have been made and a number of new generators are being flight-endurance tested. In addition, other types of flexible accessory drives are being investigated to determine what further improvements can be made.

Effect of Vibration on Accessories

Vibration may interfere with satisfactory operation of the electric system by causing faulty operation of relays and regulators and by causing mechanical failures which render relays, regulators, and generators inoperative.

Vibration may cause chatter of reverse-current relays due to resonant vibration in springs or excessive acceleration of contact members. Generator voltage regulators, as installed on rubber mountings, usually have natural frequencies in the range from 20 to 45 cycles per second. If regulator bases are subjected to vibration at a natural frequency, the vibration will be magnified, and erratic voltage variations in the electric system may result in addition to the possibility of mechanical failure in the regulator. Possible vibration troubles in generators consist primarily of the following: excessive brush deterioration and mechanical failure of screws, mounting flanges, end shields, drive shafts and couplings, brush pigtails, cables, and cable connectors.

Sources of Vibration in Aircraft

Knowledge of vibration frequencies and magnitudes which will be encountered is a primary requisite for design of accessories. The possible exciting frequencies are indicated in the following discussion. Magnitudes of vibration can be determined only by measurement.

Vibrations in aircraft are encountered over a wide range of frequencies. The vibrations are caused by periodic forces and couples introduced by the engine-combustion cycle, moving masses in the engine and accessories, propeller dissymmetry, and air-flow disturbances. Accessory-vibration magnitudes depend upon the magnitudes of exciting forces and couples and the vibration-response characteristics of accessories and the apparatus through which the vibration is transmitted.

VIBRATION DUE TO COMBUSTION CYCLE

The predominant periodic components of torque delivered to the crankshaft by a single cylinder of a four-stroke-cycle gasoline engine are approximately twice the average torque delivered by the cylinder. In a multicylinder engine the more severe of the periodic components of torque of the individual cylinders are balanced out, contributing thereby to smoother operation of the engine.

Fourier analysis can be applied to the torque output characteristics of an engine to evaluate the periodic components. A plot of the components of torque impressed on the crankshaft by a single cylinder of a four-stroke-cycle gasoline engine[1] is shown in Figure 1. The periodic torque frequencies in a four-stroke-cycle engine are integral multiples of one-half crankshaft revolution frequency. These frequencies usually are referred to as orders or multiples of crankshaft revolution frequency.

In an ideal multicylinder four-stroke-cycle engine with identical torque characteristics for all cylinders and firing at equal time intervals, the orders of the only resultant periodic torque components would be all integral multiples of the number of explosions per revolution, their magnitudes being the same percentage of the average torque as the percentages of the same orders for the single cyclinder. In engines of large capacity torsional vibration absorbers (pendulum type) are installed on the crankshaft to absorb vibration at firing frequency. With a perfect absorber on an ideal engine the only remaining periodic torque components would be all integral multiples of firing frequency above the first.

In any real engine the successive torque impulses of individual cylinders are not identical, nor are all cylinder torque characteristics identical. These irregularities are caused by variations in fuel mixture, distribution, combustion, and

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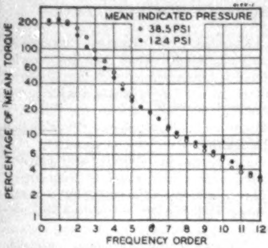


Figure 1. Periodic torque components for single cylinder of four-stroke-cycle gasoline engine with 5.28 compression ratio

Data from reference 1

ignition timing; and they also may be caused by the inherent differences introduced in piston travel by the use of a master rod in the radial engine. As a result of these variations, complete balancing of orders other than firing-frequency integral multiples does not occur, and in the four-stroke-cycle engine there may be found all frequencies which are integral multiples of one-half crankshaft revolution frequency. Orders of vibration below firing-frequency order are particularly severe under conditions of misfiring due to faulty ignition or combustion.

These periodic torques produce angular oscillations of the crankshaft, propeller, accessory drives, and the engine housing; vibrations of the engine housing are transmitted partially to the aircraft structure. Severe vibrations in the engine or accessories may be encountered when the excitation frequencies are near resonant frequencies of accessories or engine components involved.

In a radial engine a whirling vibration of the crankshaft may be produced by firing impulses. The whirling of the crankshaft is accompanied by a corresponding whirl of the engine, and the longitudinal axis of the engine generates a conical surface. This whirl becomes most severe at engine speeds, such that the resonant frequency for this mode of vibration is equal to the frequency of explosions in a single bank of cylinders plus or minus crankshaft revolution frequency. When the whirl frequency occurs at the higher-order frequency, the crankshaft whirl is in the direction of crankshaft rotation. In a seven-cylinder single-bank or 14-cylinder double-bank four-stroke-cycle radial engine, this mode of vibration would be excited at 2.5- and 4.5- order frequencies.

VIBRATION DUE TO INERTIA FORCES

Vibration at rotational frequency may occur because of dynamic unbalance in any rotating apparatus in the engine or accessories. Unbalanced inertia forces in pistons, connecting rods, and crankshaft may introduce vibration at integral multiples of crankshaft revolution frequency, particularly the first, second, and third orders. The orders above the first are caused by nonsimple harmonic relations between piston travel and crankshaft revolution.

VIBRATION DUE TO PROPELLER DISSYMMETRY

In addition to vibration due to unbalanced mass in the propeller, geometrical dissymmetry will introduce whirling vibrations at propeller revolution frequency such as would be produced by a small single-bladed propeller. Propellers on large engines usually are geared to run below engine speed, on some engines as low as one-third engine speed, introducing unbalance vibration below first-order frequency.

AERODYNAMICALLY EXCITED VIBRATIONS

Passing of propeller blades near the wing and fuselage surfaces and propeller backwash on wing and tail surfaces cause air-pressure and velocity variations which excite vibrations at frequencies which are integral multiples of the product of propeller revolution frequency and the number of blades per propeller.

Flutter of wings, stabilizers, and control surfaces may be excited by air flow at high speeds, but flutter is not encountered generally under normal conditions of operation.

Vibration Response Characteristics of Accessories

The vibration response characteristics of an accessory must be known before its performance can be predicted for specified vibrations imposed on the drive or on the attachment surface. As installed in an aircraft, the vibration on the drive or the surface of attachment will be affected by the vibration of the accessory, but laboratory investigations of response under idealized conditions are helpful in making improvements in design.

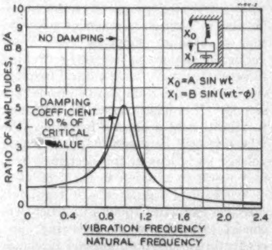


Figure 2. Steady-state vibration response characteristics of system with one degree of freedom and viscous damping

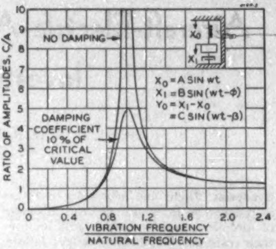


Figure 3. Steady-state vibration deformation characterisitcs of system with one degree of freedom and viscous damping

The response characteristics of an accessory depend upon its mass distribution, flexibility, and damping properties. In general, an accessory with a simple mounting has vibration response characteristics similar to those of a simple system consisting of a mass suspended on a spring and provided with a viscous damper. The vibration response characteristics of such a system are indicated in Figures 2 and 3. More complex mechanical systems have more than one resonant frequency. Damping determines the magnification of vibration at any of the resonant frequencies.

In most practical cases the damping is nonviscous, and the response characteristics differ somewhat from those of the simple theoretical case, but the same danger of large magnification of vibration at resonance exists. It should be noted that, where the exciting frequency is less than 1.4 times the natural frequency of the simple system, the vibration is magnified; and at higher frequencies the vibration is reduced. It should be observed also that damping is effective in reducing the transmission of vibration only at frequencies where the vibration is magnified.

Because of the many possibilities of resonant excitation in aircraft, it is desirable to have sufficient damping to prevent excessive vibration at resonance. Damping is present in accessory drives and mountings in the forms of mechanical hysteresis and rubbing friction on surfaces in contact in addition to that provided by damping devices.

The torsional response characteristics of a generator with a quill shaft with and without an asbestos damper are shown in Figure 4. The data on the damper assembly were observed at the conclusion of a ten-hour run with an input amplitude of one degree at the resonant frequency.

Accessory-Vibration Test Apparatus

The vibration response characteristics of an accessory generally cannot be determined except by experimental means because of the difficulty of analyzing damp-

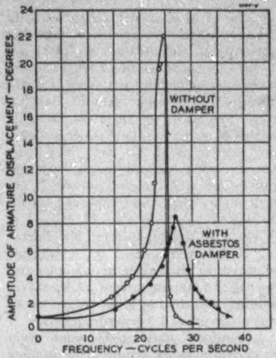


Figure 4. Torsional vibration response characteristics of model 2CM80B5 generator at one-degree input amplitude

ing properties. Test apparatus also is required for fatigue testing and observation of faulty operation caused by vibration.

The most common type of apparatus used for vibration testing of accessories is the reciprocating shake table driven by an eccentric. Such shake tables are satisfactory for low-frequency vibration, but most of these are not satisfactory for use above 60 cycles per second because of wave-form irregularities introduced by backlash in linkages. This limitation is apparently the reason for the common upper limit of 50 to 60 cycles per second in aircraft-accessory translational-vibration test specifications.

Lightweight apparatus may be tested on electrodynamic shake tables which operate in the same manner as the radio loud-speaker. These are particularly useful for calibration of vibration pickups.

The apparatus shown in Figure 5 is used to investigate the flexural vibration characteristics of aircraft generators. It illustrates a generator and other accessories installed on an engine rear cover or accessory pad which in turn is installed on a 1,600-pound steel block. Vibration of the generator is excited by a magnet coil which has a-c and d-c windings. The a-c winding is supplied by a 300-watt amplifier which amplifies an oscillator signal.

The apparatus is used for determination of generator natural frequencies and fatigue testing of mountings, end shields, and brush rigging. Initial tests have been made on devices for measurement of the vibration exciting forces and mounting flange deformations to permit evaluation of damping properties. Data on damping properties are necessary before the response of the generator to engine vibrations can be predicted at frequencies near generator resonant frequencies.

The apparatus shown in Figure 6 was made to check compliance of aircraft generators with Army Air Force torsional test specifications and to provide facilities for investigation of flexible drive and damper characteristics. The generator is driven through a universal joint which introduces torsional vibration at twice revolution frequency with amplitudes which are a function of the offset angle between the shafts.[3] The motor housing between the generator and the universal joint serves only as a shaft-bearing support. The flywheel between the driving motor and the universal joint has a large moment of inertia as compared with generator moments of inertia, so that the vibration on the drive spline of the generator will be nearly independent of the vibration of the generator. Too small a flywheel also would raise the resonant frequency of the generator on the test apparatus.

A visual means of observing the amplitude of vibration displacement of the armature of the generator was devised for measurement of response characteristics and evaluation of vibration torques. The torsional vibration indicator, shown in Figure 7, is viewed under stroboscopic illumination at four times revolution frequency. The operation of the indicator can be visualized by considering an indicator disk with a single radial line which is made visible by stroboscopic light at equal time intervals four times per revolution. The radial line would appear as two perpendicular diameters if there were no vibration or if the radial line had no lead or lag relative to a constant-angular-velocity radius vector at the instants of flashing. If the indicator were viewed when the lead and lag were maximum values, the radial lines would appear as a pair of diameters not at right angles. The angle between the diameters would be greater or less than a right angle by an amount equal to twice the maximum lead or lag of the disk relative to a constant velocity vector. The disk used for the tests has a pointer which indicates by its position on a scale the amplitude of the angular displacement.

Vibration Measurements on B-17 Flying Fortress

Measurements of vibration were obtained by oscillographic recording and wave analysis of amplified vibration detector signals. Quartz-crystal acceleration detectors were attached on generators $6^3/_4$ inches from the mounting pad as shown in Figure 8. Rochelle-crystal acceleration detectors and velocity detectors were attached to nacelle and fuselage ribs, as shown in Figures 9 and 10, to obtain measurements of vibration near generator reverse-current relays and voltage regulators.

The flight tests were conducted under various conditions of engine operation and at altitudes up to 33,000 feet. Data were not obtained over the complete range of engine operating speeds, and therefore vibrations more severe than those observed may be characteristic of the airplane.

The envelopes of the vibration frequency components observed are shown on the multilogarithmic charts in Figures 11, 12, and 13. Very little data on frequency components below 25 cycles per

Figure 5. Aircraft-generator flexural-vibration test apparatus

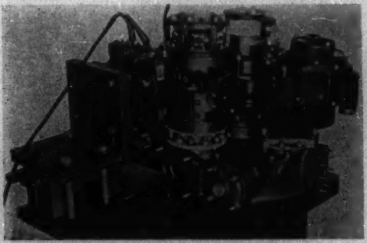
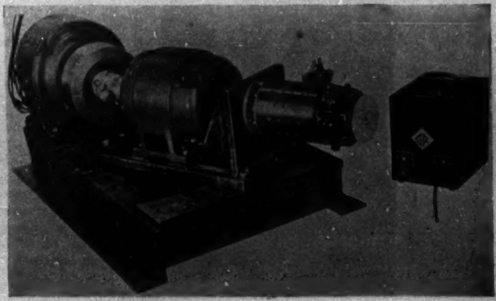


Figure 6. Aircraft-generator torsional-vibration test apparatus



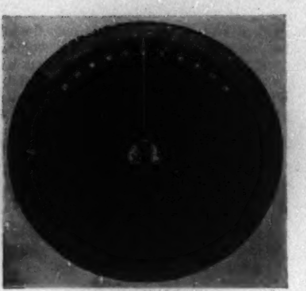


Figure 7. Torsional vibration indicator disk

second were obtained, because the wave analyzer had a lower limit of 25 cycles per second, and the low-frequency velocities and accelerations were of small magnitudes as compared with the higher-frequency components observed on the oscillograms. Low-frequency vibrations are most predominant during ground operation of the engines at low speeds.

The bending moments to which the mounting flanges were subjected at frequencies below 200 cycles per second were approximately equal to the product of the static moment because of gravity and the vertical or lateral acceleration components (expressed as times gravity). The commutator vibrations at frequencies up to 200 cycles per second are greater than those indicated by the measurements because of maximum deflections at the commutator end of the generator.

The actual flexural response of the generator at high frequencies as yet has not been determined accurately, but since the generator has two degrees of freedom in flexure it has two natural frequencies. At the lower natural frequency the magnet frame and armature vibrate in phase, and at the higher natural frequency they vibrate in phase opposition. The lower natural frequency of the General Electric model 2*CM*80*B*5 (United States Army Air Force type *P*-1) generator as mounted on an engine is approximately 150 cycles per second, and the other natural frequency is believed to be at about 300 cycles per second. Another natural frequency is four-node vibration in the magnet frame, which in the type *CM*-80 generator is 900 cycles per second. This may account for some of the high-frequency vibrations observed.

Investigation of Generator Mounting Failures

Measurements of vibration on model 2*CM*80*B*3 generators (Figure 14) on *B*-24 liberator bombers were made to determine the cause of failure of mounting flanges and the screws by means of which the flanges were attached to the magnet frame. Failures had occurred when the *H* series of the airplane with a new type of propeller was placed in production.

Measurements were initially made with quartz-crystal acceleration detectors. From these measurements it was determined that the generator flexural vibration accelerations encountered during engine starting and ground operation at engine speeds below the flight range were less severe than those encountered at engine speeds used in flight. Failures of generator flange screws occurred during the tests due to the added weight of the vibration detectors. To avoid further failures due to this cause during the tests the investigation was continued by measurement of dynamic strains in the mounting flange, using Baldwin Southwark type *SR*-4 wire strain gauges.

Flange-strain measurements were made on the *B*-24-*E* model throughout the range of flight engine speeds on the ground and at altitudes of 3,000 and 25,000 feet. Maximum flange strains were approximately the same for the three tests. Flange-strain measurements were then made on an inboard and an outboard engine on the *B*-24-*H* model at an altitude of 7,500 feet. Maximum flange strains on both engines were approximately the same and about 15 per cent higher than those observed on the *B*-24-*E*.

The most severe strains in the mounting flange were caused by a whirling vibration of the engine at 2.5-order frequency at 2,500 engine rpm. A less severe whirling vibration at lower engine speeds was observed at 4.5-order frequencies.

Laboratory static tests were made to correlate flange strains with bending moments at the mounting pad and with strains in the screws used to attach the flange to the magnet frame. Measurements of screw tensile strain as a function of tightening torque also were obtained. The maximum flange strain observed corresponds to a mounting pad bending moment of 12,000 pound-inches. The static test data showed that the maximum screw strains were caused by flexure of the screws as the flange rim rocked alternately on the inner and outer edges of the magnet frame. Analysis of the screw-strain data indicated that without stress concentrations under maximum vibration conditions the screws would have had a maximum stress of plus and minus 14,000 pounds per square inch superimposed on the initial thread root stress of 90,000 pounds per square inch. The actual stresses would be greater because of stress concentrations at the screwhead, the end of the threaded section, and the first engaged thread.

Laboratory dynamic tests showed that the natural frequency of the model 2*CM*80*B*3 generator as mounted on the engine-accessory pad was 135 cycles per second. The vibration response characteristics indicated in Figure 3 show that the flange deformation would be reduced if the flange were stiffened so as to raise the natural frequency of the generator,

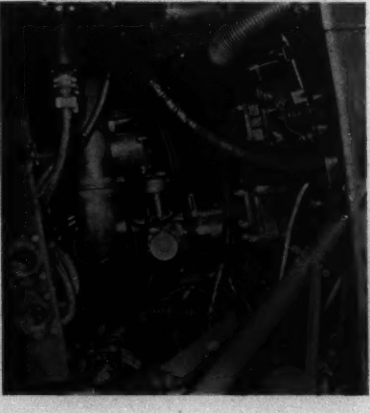


Figure 8 (left). Installation of vibration detectors on model 2CM80*B*3 generator on *B*-17-*F* bomber

United States Army Air Forces photo

Figure 9 (right). Installation of vibration detector near generator reverse-current relay in inboard nacelle of *B*-17-*F* bomber

United States Army Air Forces photo

Figure 10. Installation of vibration detectors near generator voltage regulators in fuselage of *B-17-F* bomber

United States Army Air Forces photo

since the natural frequency of the model 2*CM*80*B*3 generator was above the frequency of the most severe vibration (104 cycles per second).

Jackscrews as shown in Figure 15 were used to stiffen the mountings of generators already produced. Six jackscrews were installed with a compressional load of 1,000 pounds in each, which was sufficient to prevent loss of compression under the most severe conditions of vibration found on the engine. The natural frequency of the jackscrew generator assembly on the accessory pad was found to be 170 cycles per second. Fatigue tests and measurements of static flange strain introduced by installation of the jackscrews were made to determine the effect of jackscrews on the fatigue life of the mounting.

A new flange, shown on the generator in Figure 16, was designed to replace the one on which failures had been encountered. Internal fillet radii, flange arm thickness, and magnet-frame end thickness and diameter were increased to raise the natural frequency of the generator and to reduce flexure of screws. In addition, the contour of the ventilating holes was rounded to reduce concentrations of stress, and the number of screws used for attachment of flange to generator was doubled. Stresscoat lacquer tests and dynamic strain measurements were made to study the stresses in various parts of the flange. The natural frequency of the generator with this flange on the engine-accessory pad was 150 cycles per second.

Following the laboratory investigations of the vibration characteristics of generators with the jackscrew assembly and the new flange, twenty generators with the jackscrew flange and twenty with the new flange were installed on *B*-24 liberator bombers at an air base where the aircraft would accumulate time rapidly at high engine speeds. No failures of mountings on these generators have been reported.

To obtain quantitative data on the flight vibration performance of the jackscrew assembly and on the new flange design, another series of flight tests was made on a *B*-24-*H* bomber. These tests were made in the same manner and under the same operating conditions as the previous tests on the *B*-24-*H*.

Data for comparison of the vibration encountered on the three generator assemblies are shown in Figure 17. These data are plotted on the basis of flight strain measurements and laboratory correlation between flange strain and displacement of generator relative to the engine as measured at ten inches from the pad. The actual reductions in deformation of the mounting flange with the new flange and jackscrew assemblies are greater than those indicated by the curves, because with stiffer flanges greater percentages of total deflection are due to accessory pad deformation. The flexibility of the engine-accessory pad caused a reduction in the natural frequency of the type-*CM* 80 generator with the jackscrew assembly from 210 to 170 cycles per second. The most significant criterion for comparison of these assemblies is the ratio of the endurance limit of the mounting to the maximum vibration encountered, but very limited data on the endurance characteristics of the new assemblies have been obtained because of the difficulty of causing failures with the vibration excitation facilities which have been in use.

Investigation of Failures of Generator Drive Shafts

Aircraft generators are equipped with flexible drive shafts or couplings for absorption of torsional vibrations of the engines and prevention of drive failures because of misalignment between generator and engine splines. The most common type of flexible drive is the quill shaft which provides torsional and lateral flexibility to meet the aforementioned requirements. Generators without flexible drives have encountered drive coupling failures and shearing of lamination keyways. The theoretical vibration response characteristics of a flexibly driven generator with a viscous damper are the same as those shown in Figures 2 and 3.

Absorption of relatively high-frequency vibration is accomplished at the expense of magnification of vibration in the vicinity of the generator natural frequency. The magnification of vibration near the natural frequency is a function of the damping characteristics of the drive. In a quill drive without a damping device, damping is furnished by the generator magnetic field and internal

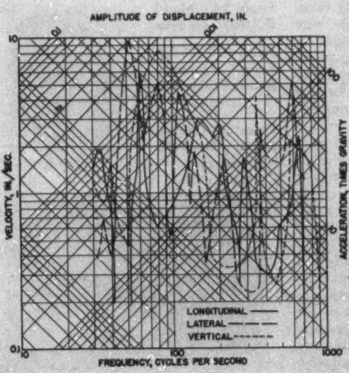
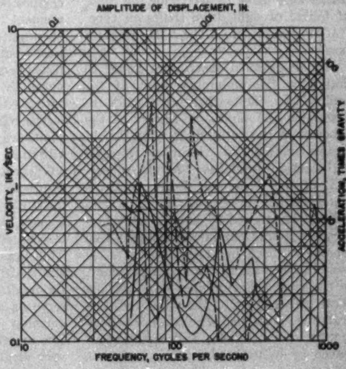


Figure 11 (left). Envelopes of peak values of vibration-frequency components observed on model 2*CM*80*B*3 generators on *B*-17-*F* bomber

Figure 12 (right). Envelopes of peak values of vibration-frequency components observed on bracket near generator voltage regulators on *B*-17-*F* bomber



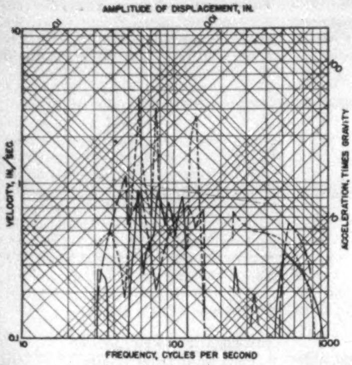


Figure 13. Envelopes of peak values of vibration-frequency components observed on bracket near inboard-generator reverse-current relay on *B*-17-*F* bomber

hysteresis in the shaft, but neither of these is adequate to assure satisfactory operation on all main and auxiliary engines. Hysteresis damping due to internal friction in the shaft does not provide sufficient damping to prevent detrimental stresses, because it does not become effective until high stresses are developed. This condition is illustrated by a test on a quill drive without field excitation or asbestos damper at input amplitudes of one-half, one, and two degrees, which resulted in resonant armature amplitudes of 16, 20, and 23 degrees respectively.

The actual vibration response characteristics of the quill drive differ from those of a simple system with viscous damping because of backlash in splines and gearing in addition to the effects of nonviscous damping. Backlash causes a sharp reduction in magnification above the natural frequency when no load torque is present or when vibration torques exceed the load torque.

Investigations of generator-torsional vibration response and durability characteristics and flight vibration measurements were made to determine the cause of failure of quill drive shafts on type-*CM*-80 generators on the *B*-24 Liberator bomber.

Initial torsional tests were made to determine the endurance limit of the quill shaft. Tests then were made to determine the limits on the fit of the asbestos in the outer shaft which would assure satisfactory vibration response and damper durability. At the suggestion of the Army Air Forces a stress concentration in the commutator end spline was relieved by using a sleeve instead of a shoulder in the spline. This change resulted in approximately a 20 per cent increase in the endurance limit of the shaft. The original shaft and the shaft with a sleeve on the spline are illustrated in Figure 18. The effect of surface compression of shafts and splines produced by induction hardening and shot blasting is being investigated. The effect of such treatments was shown by test to be a considerable increase in shaft durability at stresses above the endurance limit with smaller increases in the endurance limit itself.

Torsional vibration measurements on generators and generator drive splines on a *B*-24-*E* bomber were made to determine the vibration introduced by the engine and to correlate the laboratory vibration-response measurements with the vibration response of the generator as installed on the engine. Vibrations were measured with a detector which generates a voltage signal proportional to the velocity of torsional vibration. Oscillograms of torsional-vibration velocity and displacement were made, the latter being made by electrical integration.

Measurements of vibration were made during engine starting with ground operation of the engines from 600 to 2,500 rpm, and in flight at engine speeds from 1,600 to 2,700 rpm (take-off rpm). Measurements were made on a pair of engines on one side of the airplane, these engines being synchronized during measurement of vibration. Engines were operated at maximum recommended intake manifold pressures during flight tests, because the periodic torques produced by the combustion cycle increase with the torque developed by the engines.

The vibration causing failure of the shafts was found to be a one-half-order vibration component which reached a 1.6-degree peak amplitude of angular displacement on the generator drive at 2,700 engine rpm. This component decreased with reduction in rpm, being 0.25-degree amplitude at 2,400 rpm. This measurement was made on a special stiff shaft without armature or commutator mounted in a generator housing to serve as a means of attachment of the detector to the generator drive pad. The angular inertia of the assembly was very small as compared with that of a generator armature. The large amplitude of vibration apparently was caused by a vibration in the engine or propeller which was excited near resonance by one-half-order engine-torque components.

The envelopes of the frequency components observed on the generator drives and test generators are shown in Figure 19. Components below 20 cycles per second were not determined accurately because of the vibration-detector resonant frequency at 8 cycles per second and detector transient signals introduced at 16 cycles per second. The detector was found to generate signals due to flexural vibration such as is encountered by the generator in flight. The flexure signals obtained during laboratory test were small as compared with signals recorded during flight tests, and it is not believed that appreciable errors were introduced at frequencies below 200 cycles per second.

The model-2*CM*80*B*5 generator has a natural frequency of 23-25 cycles per second or higher, depending upon the reduction in flexibility caused by the asbestos damper. Experimental response curves for these generators when tested without dampers indicate that the maximum one-half-order vibration observed on the engine would be magnified to a displacement amplitude of 10 to 20 degrees or more, the variation being due to small variations in generator natural frequencies. Similar magnification would be characteristic of a generator in which the damper was ineffective due to wear or improper fit on assembly. At maxi-

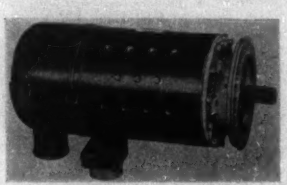


Figure 14. Model 2CM80*B*3 generator

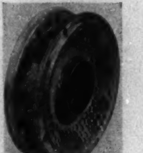
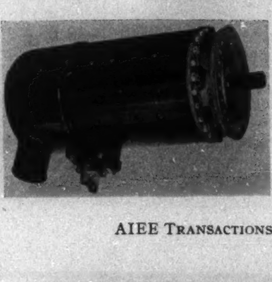


Figure 15 (right). Aircraft - generator mounting flange with jack-screws

Figure 16 (below). Model 2CM80*B*5 generator



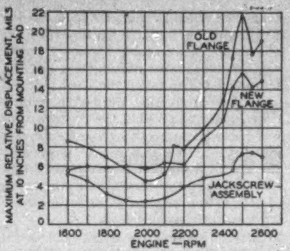


Figure 17. Vibration of type CM-80 generators with three types of mountings relative to engines on *B-24-H* bomber

mum one-half-order frequency the shafts which had failed in service were capable of withstanding an armature displacement amplitude of 12 degrees without failure for an indefinite time. At 20 degrees amplitude and the same frequency, the shaft would be expected to last one hour.

The flight measurements of torsional vibration on generator armatures were made on generators with commutators removed and magnet frames shortened to provide clearance between the torsional vibration detector and the engine lubricating oil tank. Two test assemblies were used, one with and one without an asbestos damper. The resonant frequency of these was 26 cycles per second with response of four and six degrees amplitude at 22.5 cycles per second at one degree input amplitude on the damped and undamped assemblies, respectively. The observed magnification of one-half-order vibration on these assemblies was much less than that indicated by the laboratory tests, though the magnification of the assembly with the damper was less than that of the other. The reduced magnification is believed to have been caused by backlash in splines and gears. If the reduction in magnification was due to backlash, it would not have been observed on electrically loaded generators until vibration torques were as high as the load torques.

The apparent solution to the problem of generator shaft failure on the *B*-24 is an increase in the torsional natural frequency of the generator with those improvements in shaft-endurance limit and damping properties which can be accomplished without major design changes. Flight endurance tests are now in progress on a number of generators with strengthened shafts and higher natural frequencies to verify the conclusions which have been drawn on the basis of the laboratory and flight tests.

Most failures of quill shafts and couplings on main engine-driven generators apparently have been caused by resonant vibration in the mechanical system composed of the propeller, the crankshaft, and the engine-driven-supercharger shaft and impeller. Maximum amplitudes of vibration are encountered on the generator drive when this system is resonated in its simplest mode of vibration. In this mode of vibration there is a node between the propeller and the crankshaft, with amplitudes of vibration increasing with distance from the node. The range of resonant frequencies for this mode of vibration on present double bank engines is reported by the Army Air Forces powerplant laboratory to be 20 to 30 cycles per second.

Failures of generator drive shafts and couplings on auxiliary-engine installations apparently have been caused by generator resonance to low-frequency vibratiom torques which are characteristic of an engine having a small number of cylinders. Most generators have torsional natural frequencies from 20 to 40 cycles per second—a range which results in absorption of the predominant vibrations of the main engines, except for low-frequency vibrations which may be magnified by resonances in the engine.

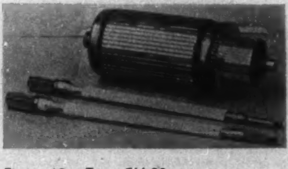


Figure 18. Type CM-80 generator armature with original and improved quill shaft

A generator having a torsional resonant frequency of 40 cycles per second as installed on a two-cylinder four-stroke-cycle gasoline engine would encounter resonant first- and second-order vibration at 2,400 and 1,200 crankshaft rpm respectively. The periodic torques causing the vibration would increase with load on the engine and would be about 200 and 160 per cent of average torque for the first and second orders respectively. Less severe vibration would be encountered below 1,200 rpm with resonant excitation at third, fourth, and higher integral orders. Resonant vibration would be excited at $^1/_2$, $1^1/_2$, $2^1/_2$, and higher orders with magnitudes of exciting torques being dependent upon the design and the operating condition of the engine. The operating speed of an auxiliary engine should be chosen to avoid resonant vibration at operating speed, or a flexible coupling should be installed in the engine to bring the generator natural frequency below operating speed resonances. The use of a damping device will reduce the possibility of failures due to vibration during starting, idling, and warming-up of the engine.

Recommendations for Design of Accessories

In general it is advantageous to design accessory mountings and drive couplings to provide low natural frequencies for absorption of vibrations characteristic of the airplane. Any mounting or drive coupling should have sufficient damping to prevent excessive vibration at resonance. Resonant excitation at any accessory resonant frequency is likely under some operating condition of the airplane.

Ideal mountings and drive couplings for accessories would provide natural frequencies below the minimum one-half-order frequency encountered in flight, and would have sufficient damping to prevent excessive resonant vibration during ground operation. Adequate damping is in some cases difficult to obtain, and it may in some installations be found practicable to use soft bumpers to reduce impact forces when vibration displacements reach the limits of the clearances provided.

Engine manufacturer recommendations and experience with various models of generators indicate that the lowest flexural natural frequency of an engine-

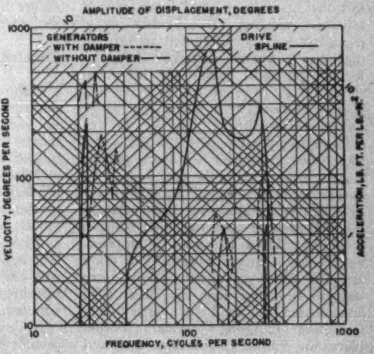


Figure 19. Envelopes of peak values of vibration frequency components observed on generator drive splines and armatures on *B-24-E* bomber

Vacuum-Tube Radio-Frequency Generator—Characteristics and Application to Induction-Heating Problems

T. P. KINN
MEMBER AIEE

Synopsis: Induction heating by radio frequency is taking its place rapidly in many industrial processes. The high-power vacuum tube, in the past used only in radio applications, is now generating radio-frequency energy for industrial use. To apply this energy properly to industrial-heating problems, it becomes necessary that engineers active in all phases of industry understand the characteristics and limitations of the vacuum-tube radio-frequency generator. The fundamentals of the vacuum-tube self-excited oscillator and design considerations which determine the characteristics of the radio-frequency generator are reviewed and illustrated. In general, the characteristics show a high-impedance constant-current variable-voltage generator which requires manipulation of load circuits to load the generator properly. Methods are illustrated for accomplishing proper loading, and numerical examples are given illustrating the formulas and procedures necessary to any induction-heating problems.

BECAUSE of wartime conditions radio-frequency heating has had a chance to demonstrate that it can be a very useful tool. The vacuum tube used as a generator of this radio frequency therefore has placed itself in industry along with the more common generators of electric energy. The vacuum-tube radio-frequency generator, like any other piece of electric equipment, has its characteristics, and these characteristics dictate its uses and limitations. It is the purpose of this paper to define some of these characteristics and show how to apply the radio-frequency generator properly to induction-heating problems.

The phenomenon of producing heat by an alternating magnetic field was known as far back as the 1880's. In 1890 Colby was granted a patent for heating in this manner. In 1900 it is believed the first practical induction furnace was placed in operation by Kjellin. It has taken the ensuing years to produce electric equipment suitable for supplying the alternating current at various frequencies for the purpose of induction heating. However, it has taken the need for "all-out" war production during these past three years to provide the incentive for widespread use of this type heating in industry. With this rapid advance in the use of induction heating the use of the vacuum-tube oscillator as a source of alternating power for induction heating has made great strides.

Except in isolated cases, the small amount of induction heating used in industry prior to the present war used power obtained from rotating machines or spark-gap oscillators. The vacuum-tube radio-frequency generator, along with the vacuum tube itself, is now showing its usefulness in industry. This is evidenced by the publicity electronics is now receiving. Because the vacuum-tube radio-frequency generator has been confined to the radio field up to this time, its operation and its characteristics are not too well known to those not connected with the radio industry.

Induction heating is now being done at frequencies from 60 cycles to 10,000 cycles and higher by rotating machinery. The rotating machine is a common source of power, and therefore its use and limitations for induction heating are simple. Service and maintenance problems are well established.

The spark-gap oscillator finds its most useful range of frequencies between 20 and 150 kilocycles. This type of generator is very useful for certain specific application. The main advantages are its simplicity and ease of operating technique, while its limitations are power output and reliability. New developments surrounding the spark gap itself which

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mounted generator should be above 150 cycles per second as installed on the engine. In the design of accessories to be installed on engines it is important that localized bending stresses and stress concentrations be minimized to provide maximum stiffness and strength. Damping in generator mountings has been shown by test to be due largely to rubbing friction between the mounting flange and the magnet frame.

Generator flexible-drive couplings and shafts are deformed by load torque, thereby complicating the design of a low-natural-frequency drive. Experience and available information on engine characteristics indicate that the most suitable torsional natural frequency is approximately 35 cycles per second. The drive assembly which was shown by laboratory tests to be better than any other type investigated is a quill shaft with a friction-plate damping device. Friction plates are splined to generator quill and outer shafts at the drive end to provide frictional losses for reduction of resonant vibration. Sintered metal plates such as are used in brakes and clutches are well adapted to use in the damper.

Conclusions

Failures and faulty operation in the electric systems of aircraft can be reduced to negligible proportions by improvements in design which can be accomplished with the aid of an adequate flight and laboratory-test program. It is of primary importance that vibration measurements on accessories in new aircraft models be made before large numbers of accessories are manufactured for use on the new models. Such measurements will eliminate the need for corrective measures on accessories already distributed.

It is expected that the result of the present program will be generators which will be suitable for installation on any present and near-future main or auxiliary engines. It is hoped that generators can be improved to the extent that servicing of generators seldom will be required except at the time of removal of engines for overhauling.

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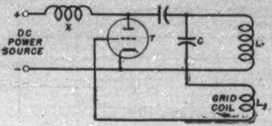


Figure 1. Basic oscillator circuit

are appearing now and will appear after the war will help better the output and reliability of this type generator.

The scope of both the rotating machine and the spark-gap generator is limited, and it is for this reason that the vacuum-tube radio-frequency generator has stepped into the picture to pick up where these other machines leave off. The vacuum-tube oscillator can do many of the jobs now being done by the rotating machine or the spark gap and in addition many more jobs which neither of these types can accomplish. At the present time, when a job can be done by either rotating machine or a vacuum-tube generator, the initial investment cost is two or three to one in favor of the rotating machine. This is primarily due to the well-established manufacturing procedures and facilities for the rotating machine. Postwar use of the vastly expanded radio facilities plus ever-increasing demand will reduce this difference rapidly.

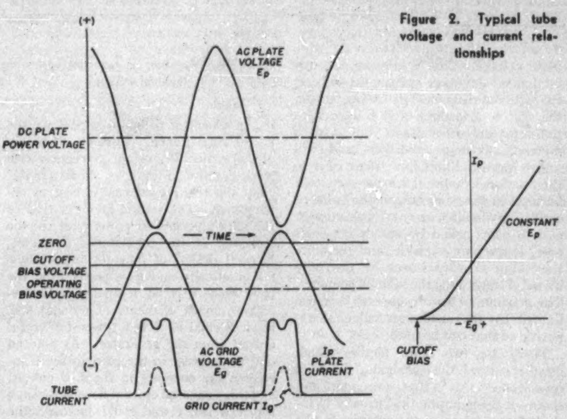


Figure 2. Typical tube voltage and current relationships

2A

2B

Radio-Frequency-Generator Characteristics

The vacuum-tube generator is a brand new device to the average user, and as with any other new technical device a certain amount of study is necessary in order to enable the average user to understand its operation and application. The vacuum-tube self-oscillating generator is fundamentally a simple device as evidenced by the circuit shown in Figure 1. Tube *T* acts as a valve or switch which is used to take energy from the d-c source at regular intervals and supply it to the tuned circuit made up of inductance *L* and capacitance *C*. The grid of the tube acts as the control knob of the valve and determines when the valve shall be shut off and when it shall be open. The valve is opened for a short portion of a cycle of the frequency determined by the circuit *L* and *C*. The circuit *L* and *C* will then oscillate just as any undamped circuit will oscillate. The vacuum-tube supplies energy to maintain this oscillation during a short portion of each cycle. The grid obtains its controlling voltage at the right phase relation to accomplish this by induction to coil *L*-1, and the oscillator therefore becomes known as a self-excited oscillator, because the grid is excited (as the field of a generator is excited) by power from the oscillating circuit *L* and *C*. If the grid-control voltage should be supplied by or from a separate source, then the generator would be known as an amplifier to differentiate from the self-excited type. The self-excited type of generator is the one most commonly used for industrial purposes.

There are many types of self-excited-oscillator circuits, but they all reduce to the fundamental circuit of Figure 1. The circuit shown in Figure 1 and the associated Figures 3, 7, 8, and 9 has been reduced to the basic fundamentals by elimination of filament supply, bias supply, plate supply, and control circuits normally associated with a complete oscillator.

The spark-gap oscillator differs very little from the vacuum-tube oscillator. By replacing the tube with a spark gap which is capable of self-quenching or extinguishing itself at a sufficiently rapid rate to permit the *L-C* circuit to oscillate, the action becomes the same as for the tube. The gap acts as the valve but no grid is necessary as in the vacuum tube because of the self-starting and extinguishing characteristics of the arc. The limit of frequency in the spark-gap-type oscillator is determined by the time required to extinguish or quench the arc. The frequency at which the vacuum tube will function as a valve is unlimited from this standpoint.

In order for a self-excited vacuum-tube oscillator to oscillate properly, there must be sufficient stored energy in the *L-C* circuit to maintain good regulation throughout a complete cyle. See Figure 2.

The grid voltage and bias voltage (fixed grid voltage) are adjusted in value and phase so that the condition shown in Figure 2A exists. Plate current flows only during that portion of the cycle where the effective plate voltage E_p is at a minimum. During the rest of a cycle any a-c energy required from the circuit must be supplied by the *L-C* circuit. To produce this condition, the kva/kw ratio must be kept high. The larger the capacitor *C*, the more stored energy and/or the more circulating current in the *L-C* circuit. The higher the current in this circuit, the greater will be the kva/kw ratio.

Most induction-heating problems resolve themselves into the number of ampere turns necessary to produce the desired heating. For this reason it is almost always necessary that the generator be capable not only of supplying power (kilowatts), but also of supplying a maximum of current flow. The current available from a vacuum-tube self-excited oscillator is dependent on the value of the capacitance *C* in the oscillator circuit and the voltage across this capacitor. The relation kva/kw, therefore, becomes a figure of merit or means of easily describing the capabilities of a vacuum-tube generator. This value of kva/kw should

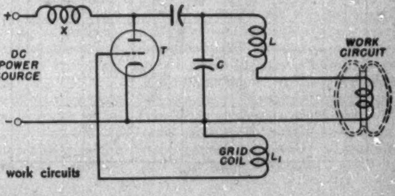


Figure 3. Oscillator and work circuits

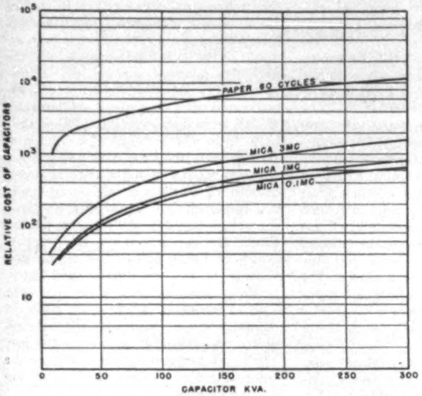


Figure 4A. Variation of capacitor cost with kilovolt-ampere requirements

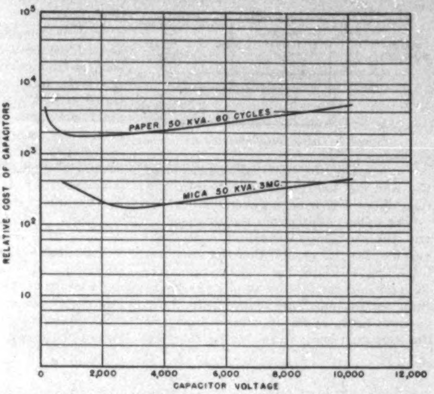


Figure 4B. Variation of capacitor cost with terminal voltage

be as high as practical, and in addition the current portion of the numerator should be as large as practical. Figure 3 illustrates the usual method of utilizing the oscillator-*L*-*C*-circuit current for induction heating by placing the work circuit in series with the oscillator inductance *L*.

The radio-frequency voltage across the oscillator *L*-*C* circuit is a direct function of the plate voltage applied to the vacuum tube. From Figure 2A it can be seen that the peak of the alternating voltage is usually slightly less than the direct plate voltage and the rms value normally runs approximately 65 per cent of the direct plate voltage. This is only an approximation, as the exact voltage depends on the characteristics of the tube in use, the degree of loading, grid-voltage relationships and other items, but it does represent average conditions and will suffice for this discussion. If we refer to the relation kva/kw it can be seen that for a given tube and output the value of oscillating-circuit current (tank current as it is often called by the design engineer) is the only variable, and therefore the value of capacitance is the only means of controlling the kilovolt-amperes. The maximum kilovolt-amperes then are limited by the maximum value of capacitance that can be used.

There are two main factors which usually control the maximum value of capacitance. One is that of economy and space—it being impractical usually to include large values of capacitance in the oscillator, because of initial cost of the capacitors and the added space required in the generator to house or package the added capacitance. Further discussion of this subject is given in connection with work circuits. Figure 4A shows graphically the relative increase in cost of capacitance. The second factor is that of the allowable loss in power that can be tolerated in the oscillator *L*-*C* circuit. Normal design of vacuum tubes allow for practically no excess power from the tube which may be dissipated in the oscillator-circuit elements (*L*-*C*) and still leave normal expected power for useful output from the generator. As pointed out, an increase in the capacitance *C* increases the currents in the *L*-*C* circuit, and this increased current flowing through the capacitor *C* and coil *L* increases the

Figure 5. Variation of oscillator output with $\frac{\text{tank circuit kva}}{\text{work circuit kw}}$ ratio

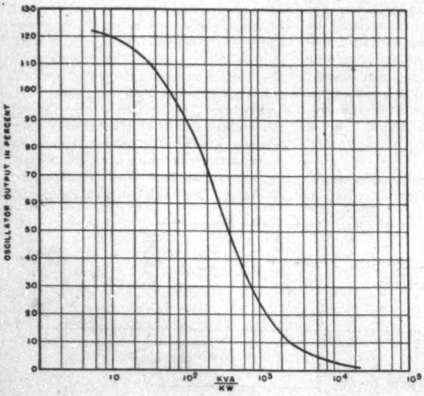
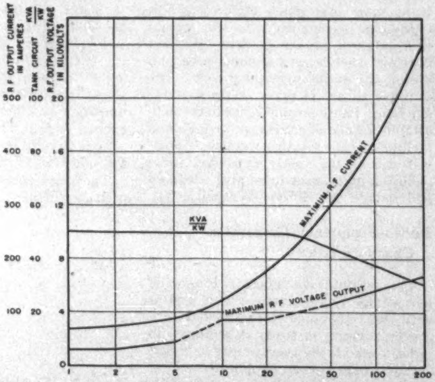


Figure 6. Typical oscillator characteristics



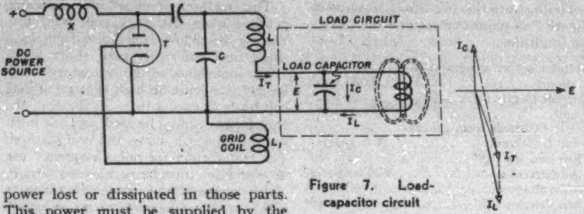


Figure 7. Load-capacitor circuit

power lost or dissipated in those parts. This power must be supplied by the vacuum tube and is not useful output. This power loss therefore must be kept as low as possible to provide the maximum useful output from the generator. Figure 5 illustrates this condition and is plotted from data on an actual 10-kw equipment.

From Figure 4B, it is obvious that high current can be obtained more economically at low values of voltage. It therefore would seem desirable to use vacuum tubes which operate on low values of plate voltage, but here we run into the fact that the vacuum tube is basically a "high-impedance device" and consequently requires high voltages to get the power desired. The designer of the radio-frequency generator and also the designer of the vacuum tube itself therefore is required to use high-voltage and high-impedance circuits.

In general, the plate potential used for the operation of vacuum tubes increases with the power output of the tube. This condition requires the use of high-current high-voltage high-frequency low-loss capacitors in the oscillating circuit of the oscillator. Such capacitors at the present time are constructed from mica, pressurized gas, paper, or oil as the dielectric material. Each type has its construction limitations which restrict the kilovolt-ampere ratings to rather low values compared to that needed in the higher-powered oscillators. The conventional oil and paper dielectric capacitors have relatively high power factor at the radio frequencies involved, with the result that power dissipated in such capacitors is abnormal and cannot be tolerated. The mica and compressed-gas capacitors have sufficiently low power factor to permit reasonable losses in the capacitor at high frequencies, but to date physical limitations in construction restrict these types of capacitors to relatively low values of capacitance and current-carrying capabilities. The design engineer must keep these limitations in mind with the result that present-day vacuum-tube generators for induction-heating purpose in the frequency range of 100 to 550 kilocycles have characteristics approximately as shown in Figure 6.

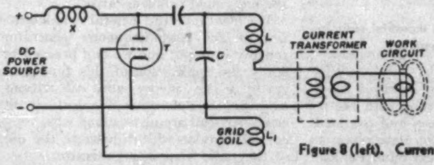


Figure 8 (left). Current-transformer circuit

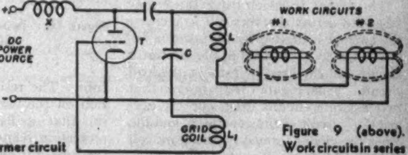


Figure 9 (above). Work circuits in series

It will be noted that as the power output of the vacuum-tube radio-frequency generator gets above 50 kw the effective kva/kw ratio drops off, primarily because of the limitations in oscillator-capacitor capabilities. As future developments in the capacitor field are made, this condition will be corrected or bettered. The curve of radio-frequency voltage shown in Figure 6 represents the maximum usable radio-frequency voltage from the generator and, as already explained, is dependent on the plate voltage necessary on the vacuum tube. This, of course, will vary with the type and make of vacuum tube used, but in general the curve is representative of what generally can be expected. The maximum radio-frequency-current curve is representative of current available and as pointed out previously, is dependent on the allowable power loss that can be allowed in the capacitor and coil of the oscillating circuit and the plate voltage necessary on the vacuum tube.

The kva/kw curve shown in Figure 6 is for full output from the radio-frequency generator. Quite often full power capabilities are not required from the generator with the result that the kva/kw ratio increases in direct proportion to the drop in power requirements. It becomes possible, by taking advantage of this characteristic, to supply power to excessively high kva/kw ratio loads.

The generally accepted frequency range for the radio-frequency generator for induction-heating use lies between 100 and 550 kilocycles. The upper limit of this range has been set arbitrarily for two basic reasons. The frequencies between 550 and 1,500 kilocycles are occupied by the broadcast stations of this country, and it generally has been accepted as good practice to keep industrial generators from operating in this frequency range to eliminate the possibility of interference to broadcast reception. Except for special case-hardening problems where extremely shallow depth of penetration is necessary, the frequencies above 1,500 kilocycles normally are used for dielectric heating rather than induction heating. This leaves 550 kilocycles as the normally accepted upper frequency limit for induction heating.

For a given set of conditions it is possible to raise the kilovolt-amperes in a radio-frequency generator by raising the frequency because lower capacitive reactance and resultant higher generator current can be obtained as the frequency is increased. The majority of radio-frequency generators, therefore, operate at frequencies from 400 to 550 kilocycles to take advantage of this increase in kilovolt-amperes. This range of frequency is adequate for practically all types of induction-heating problems and provides the most economical design.

In all rotating-machine problems the load is adjusted to a point where efficiency and output satisfy the ratings of the generator. This same procedure is necessary for proper and efficient operation of the radio-frequency generator. The rotating machine and spark-gap oscillator are both generators with low internal-impedance characteristics. The majority of induction-heating loads are also low impedance with the result that adjustment for proper efficiency and full power from the generator is relatively simple. The radio-frequency generator has an inherent high-impedance characteristic, and to apply this type generator to the low-impedance induction-heating loads it becomes necessary to obtain a suitable impedance match between the load and the radio-frequency generator.

In the case of the rotating machine full power is obtained from the generator by power-factor-correction capacitors connected across the load or by use of step-down transformers. In the case of the radio-frequency generator the solution is handled in much the same manner. The usual method for taking power from a radio-frequency generator is illustrated in Figure 3. It is necessary first to arrive at a suitable coil design which will allow rated power to be taken from the generator with the current available from the generator. This is accomplished by suitable selection of ampere turns and spacing between the work and the coil. In general, the number of turns in the work coil is selected to give the desired ampere turns. Many jobs are such that the impedance match and ampere-turns requirements cannot be met because of physical limitations. For instance, it is often physically impractical to get sufficient number of turns in the work coil because of the shape of the piece to be heated. Then again, the shape of the material may restrict the proximity between work and the work coil. This latter condition normally is referred to as coupling—with close proximity between coil and work we have "tight" coupling, and when the coil is a considerable distance from the part to be heated we have "loose" coupling. To correct the condition where insufficient ampere turns are available to load the radio-frequency generator properly there are two or three methods which may be used.

The first of these methods is similar to that used in the case of the motor generator set; that is, we connect capacitance across the work coil to increase the current in this coil. This is a form of power-factor correction, as we are actually partially tuning the work-coil circuit to resonance with the generator frequency in order to realize an increase in current in the work coil. Figure 7 illustrates this circuit and the approximate phase relation of voltage and current that exists in this circuit.

The extent to which capacitance may be added across the work coil to increase the current is limited by the relation between the kva/kw ratio in the generator circuit and the kva/kw ratio in the load circuit. From Figure 7 it can be seen that two separate circuits exist, one the normal *L-C* circuit of the generator, and the other the circuit formed by the work coil and the added capacitor across this coil.

Oscillations in a vacuum-tube oscillator normally take place around the circuit with the lowest loss. When more than one circuit exists, as is the case in the circuit of Figure 7, it is absolutely essential that the main-oscillator *L-C* circuit remain the controlling circuit. Circuit loss normally is expressed by the value "Q" $= \omega l/r$ and is therefore a function of reactance and resistance. If we multiply this expression by I^2 we have $\omega LI^2/rI^2 = EI/w$ or kva ÷ kw. The kva/kw ratio therefore can be used as a measure of circuit loss, and the higher this ratio the lower the loss. For the main-oscillator *L-C* circuit to retain control of oscillation its kva/kw ratio therefore must be larger than that of the load circuit. In this case the value of kilowatts is the same for both circuits and is taken as the total power dissipated in the load circuit.

To illustrate this condition let us keep Figure 7 in mind and assume the following conditions:

Voltage across generator *L-C* circuit	5,000 volts
Current in generator *L-C* circuit	100 amperes
Power available from generator *L-C* circuit	10 kilowatts
Then kva/kw "Q"	50
Load current	500 amperes
Load voltage	1,500 volts
Then kva/kw "Q"	75

Under these conditions unstable operation will result, because the oscillator will attempt to operate around the load circuit, but, since this circuit requires only 1,500 volts to produce full load of ten kilowatts in the work circuit, it can be seen that the circuit is of too low impedance to match properly the normal radio-frequency plate voltage across the vacuum tube. The result is an overload condition on the oscillator tube with the result that oscillations stop and overload-protection relays remove plate power to the tube. If on the other hand the load coil could be designed to produce full power of ten kilowatts at 500 amperes and 800 volts, the kva/kw ratio then would be 40 which is 10 less than the generator circuit, and stable operation around the generator *L-C* circuit will result. The vacuum tube will be operating into its proper impedance and at the proper frequency. It has been found in practice that the work circuit kva/kw (Q) always should be 80 per cent or less than the generator kva/kw ratio.

Figure 10. Two-kilowatt industrial radio-frequency generator

The second method which may be used to increase the load-circuit current is shown in Figure 8. A transformer is placed in series with the generator oscillating circuit. This transformer is a step-down transformer with a high-current low-voltage secondary (low-impedance) winding to match the low impedance of a high-current load or work circuit.

This method of impedance matching is ideal when the work coil must necessarily be a low impedance such as a single-turn coil with very short leads. If the impedance of the secondary winding gets the least bit high because of long coil leads or excess turns in the coil, the transformer cannot be used, and one must use the capacitor across the load method. The transformer for radio-frequency use of necessity must be an air-core design, which means the leakage reactance is high and the efficiency poor. A guide as to when a current transformer is desirable can be obtained from the load circuit kva/kw ratios. If this ratio is in the order of 10 or less and the current necessary is from three to five times that available from the generator, the current transformer will provide the best impedance match and performance.

Still another method of impedance match quite often can be used to advantage when the impedance of the work circuit is low. It is possible to series two or more of the work circuits when the nature of the work permits the heating of more than one piece at a time. To do this it may be necessary to increase the heating time of an individual piece, but the effective heating time may be lower than for one piece, because more than one piece is being heated simultaneously.

Figure 9 shows this type of proposed connection. This is an especially useful method when the radio-frequency generator is of a higher rating than is necessary to perform the desired work.

Combinations of the foregoing impedance-matching systems of course are possible and will present themselves as the individual problems arise.

We now have the general characteristics of the radio-frequency generator roughly in mind, and we can proceed to study the application of this type generator. The theory and calculations necessary to determine the work-circuit characteristics are given along with typical calculations which illustrate the use of the radio-frequency generator.

Radio-Frequency-Generator Application

In general, when considering an induction-heating problem, we are confronted with finding the answers to the following questions:

1. What is the rating of the oscillator best suited for the job?

2. What extras in the form of power-factor correction or impedance matching are necessary?

3. What is the general design of the work coil?

The first step in calculation requires the finding of the power density (watts per cubic inch) required to accomplish the heating.

$$\text{Power density} = \frac{\text{thermal power in watts}}{\text{volume of metal in coil}}$$

$$\text{Thermal power} = 1.76 \times 10^{-2} MC\Delta T = \text{kilowatts} \qquad (1)$$

where

M = rate of heating in pounds per minute
C = specific heat of material
ΔT = temperature rise in degrees Fahrenheit

Power density is computed for a hollow cylinder on the basis of the volume of a solid cylinder of the same diameter. This is necessary, because, as far as eddy current configurations and power input are concerned, hollow and solid shapes of materials behave identically. Skin effect limits the depth of penetration of currents into the work with the result that the metal beneath this depth of penetration can have no electrical effect.

The second step in calculation is that of finding peak magnetizing force required. The following relationships give this quantity for several simple shapes into which nearly any problem in induction heating can be resolved:

Magnetic cylinder:

$$H_o = \left(\frac{3.64PD \times d \times 10^3}{\sqrt{\rho f}}\right)^{2/3} \qquad (2)$$

Magnetic strip:

$$H_o = \left(\frac{7.3PD \times t \times 10^3}{\sqrt{\rho f}}\right)^{2/3} \qquad (3)$$

Nonmagnetic cylinder:

$$H_o = \left(\frac{61.3PD \times d \times 10^4}{\sqrt{\rho f}}\right)^{1/2} \qquad (4)$$

Nonmagnetic strip:

$$H_o = \left(\frac{1.23PD \times t \times 10^6}{\sqrt{\rho f}}\right)^{1/2} \qquad (5)$$

where

PD = power density in watts per cubic inch
t = strip thickness in inches
d = cylinder diameter in inches
H_o = peak magnetizing force in oersteds
f = frequency in cycles
ρ = resistivity at minimum temperature in microhm-centimeters

It will be noted that the resistivity was specified at minimum temperature. This is necessary to insure maximum power (H_0^2) into the work at the start of the heating cycle. As can be seen from the foregoing formula for H_0, the magnetizing force necessary for a given power density will drop as resistivity (ρ) increases with temperature.

Figures 12 to 15 have been plotted for the magnetizing-force relationships in order to assist in calculations.

The third step is that of determining voltage and current required in a coil to provide the required magnetizing force. Coil dimensions must be assumed in correspondence with the conditions of the problem.

Figure 11. A 200-kw industrial radio-frequency generator

Voltage and current in terms of magnetizing force are given as

$$E = H_o(2.86 \times 10^{-7} fNAK_2) \qquad (6)$$

$$I = H_o\left(1.43\frac{l}{N}K_1\right) \qquad (7)$$

where

E = coil voltage in volts rms
I = coil current in amperes rms
H_o = peak magnetizing force in oersteds
N = number of turns in coil
A = cross-sectional coil area in square inches
l = coil length in inches

The constants K_1 and K_2 are factors by which coil current and coil voltage, respectively, are modified to correct for small length-to-diameter ratio of the coil and for the effect of metal in the field of the coil. These factors are based upon theoretical and empirical considerations and are plotted in Figure 16, together with a nomograph for determining current and voltage using the foregoing relationships.

The coil-current factor K_1 is a function of the length-to-diameter ratio of the coil, and shows that in order to reduce the current required for a given magnetizing force this ratio should be as large as possible.

The coil-voltage factor K_2 is a function of the ratio of work-diameter-to-coil-diameter coupling and indicates that at oscillator frequencies the coil voltage decreases as the diameter of the work is increased relative to that of the coil (tightly coupled). The effect is essentially the same for both magnetic and nonmagnetic cores at oscillator frequencies.

All that remains to complete the analysis is to determine the rating of the oscillator to be used. If current requirements are greater than can be provided by an oscillator of the correct power rating, impedance matching as described previously is necessary.

If we refer to Figure 7, the rated oscillator current is shown as I_T. If the work coil, L, is partially tuned as indicated, the current in the tuning capacitor I_C will be very nearly 180 degrees out of phase with the coil current I_L. A work-circuit capacitor therefore is used with a reactance such that

$$I_c = I_L - I_T \qquad (8)$$

The total power rating which must be provided by the generator will be the sum of thermal power generated in the work and the I^2R coil loss.

Loss in the work coil is given[1] by the empirical formula:

$$W_c = 1.40\frac{d_c}{d_w}\sqrt{\frac{\rho_c}{\rho_w}} \times W_w \qquad (9)$$

where

W_c = coil loss in watts
W_w = power generated in work in watts
d_c = coil diameter
d_w = work diameter
ρ_c = coil resistivity at working temperature
ρ_w = work resistivity at minimum temperature
(Minimum temperature used to insure provision for maximum loss)

A curve has been plotted in Figure 15 from which total power required may be determined for several materials. These curves include coil losses encountered in normal heating problems and can be used as a guide to generator power requirements.

Case hardening of the surface of a cylinder will be accomplished if the depth of current penetration is less than the depth of case desired, since a major portion of the heat input is generated within the depth of penetration, and if the rate of energy input is rapid enough, so that heat conduction to the interior of the work is minimized. As an approximation, the depth of penetration will be made always less than half the case thickness desired,

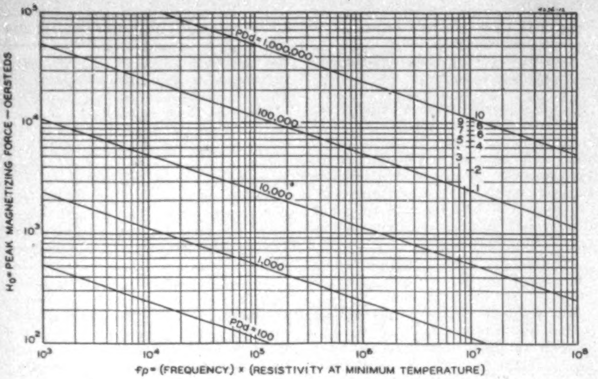


Figure 12. Magnetizing force required for magnetic cylinder

$$Wd = 2.75 \times 10^4 \sqrt{f\rho} H_o^{3/4}$$

W = power density in watts per cubic inch
d = cylinder diameter in inches
ρ = resistivity at minimum temperature in microhm-centimeters
f = frequency in cycles
H_o = peak magnetizing force in oersteds

and heat conduction will be depended upon for the remainder. The thermal problem of heat penetration into a cylinder under the conditions of case hardening is extremely complex. Simplifying assumptions therefore will be made for practical results. On the basis of experimental work, it has been found that a minimum power density of two kilowatts per square inch of surface area to be hardened is sufficient to cause a hardened case if the heating cycle is short enough to prevent excessive conduction to the interior of the object to be heated. The power density required is dependent upon the type of hardening problem under consideration, and may vary from 4 to 10 kilowatts for normal applications to 25 kilowatts or more per square inch for special thin case hardening applications. Experimental work is required, in general, to determine the correct power.

It will be assumed further that the material remains magnetic to a temperature of 1,300 degrees Fahrenheit, and that it must be raised to a final temperature of 1,600 degrees Fahrenheit in order to produce a hardened case. If a frequency of 450 kilocycles and a resistivity of 20 microhm-centimeters at minimum temperature is used, it is found by an analysis shown in the appendix that 720 oersteds (50) is the proper value of magnetizing force to be used. Other values of final temperature, power density, frequency, and resistivity, of course, will require a similar mathematical procedure.

If the current and voltage requirements as calculated fall within the range of the radio-frequency generator, using impedance matching where necessary, and the kva/kw ratio of the work circuit is 80 per cent or less than that of the generator, it can be assumed that the problem is solved. Considerable flexibility is provided in most applications where work-coil dimensions and turns can be varied and where combinations of impedance-matching systems can be used.

When it appears impractical to do the job at hand with a generator of suitable rating, it may be possible that a rotating machine will perform the job, provided its frequency is above a minimum specified by the following relationships.

Solid magnetic or nonmagnetic cylinder:

$$\mu f = 71 \frac{\rho}{d^2} \tag{10}$$

Hollow magnetic cylinder:

$$f = 0.129 \sqrt{\frac{(PD) \times d \times \rho}{t^3}} \tag{11}$$

Hollow nonmagnetic cylinder:

$$f = \frac{7.75\rho}{t^2} \tag{12}$$

Magnetic or nonmagnetic strip:

$$\mu f = 35.2 \frac{\rho}{t^2} \tag{13}$$

Magnetic permeability at minimum frequency (in terms of μf of equations 10 and 13):

$$\mu = \sqrt{\frac{123\mu f}{PD}} \tag{14}$$

where

μ = magnetic permeability under specified conditions of equation 30
f = minimum frequency in cycles
ρ = resistivity at maximum temperature in microhm-centimeters
t = wall thickness in inches (hollow-cylinder) strip thickness (in inches)
d = outside diameter of cylinder in inches
PD = power density in watts per cubic inch

The resistivity at maximum temperature is used in the equations for minimum frequency, because the largest value of this quantity will occur when the resistivity is a maximum. The hot resistivity is computed as

$$\rho \text{ hot} = \rho \text{ cold } (1 + \alpha \Delta t) \tag{15}$$

where

α = the temperature coefficient of resistivity
Δt = the temperature rise in degrees centigrade = the temperature rise in degrees Fahrenheit/1.8

The following examples have been prepared to illustrate the procedure to follow in arriving at the answer to the three questions listed previously for various typical types of heating problems.

Example 1. Magnetic Strip

To illustrate the problem of heating a magnetic strip of steel, let us assume that we desire to fuse electroplated tin on the surface of a strip 2.5 inches wide and 0.12 inch thick. The temperature necessary to flow the tin is 550 degrees Fahrenheit, and we shall assume that the strip is traveling at a continuous rate of 15 feet per minute.

ρ = 20 microhm-centimeters (at minimum temperature)

A. Volume to be heated per minute = thickness × width × speed
= $0.12 \times 2.5 \times 12 \times 15 = 54$ cubic inches per minute

B. Weight heated per minute = volume per minute × pounds per cubic inch
= $54 \times 0.286 = 15.4$ pounds per minute

C. Thermal power = $1.76 \times 10^{-2} \times 15.4 \times 0.12 \times (550-72)$
= 15.5 kw

To determine a power density it is necessary to assume a coil length. The turns per inch and the total length must be of the proper magnitude so that current and voltage limitations are not exceeded. This must be done by a cut-and-try method. In some cases, coil length is fixed by conditions of the problem, so that the only variation possible is in the turns-per-inch ratio. In this case the coil length will be assumed to be 12 inches.

D. Volume of metal in coil (length = 12 inches) = $\frac{54}{15} = 3.6$ cubic inches

E. Power density = $\frac{15,500}{3.6} = 4,300$ watts per cubic inch

F. $PD \times t = 4,300 \times 0.12 = 516$; $\rho f = 450,000 \times 20 = 9 \times 10^6$

G. From Figure 13, $H_o = 116$

H. Assume a coil

$l = 12$ inches (length); $N = 14$ (turns); $N/l = 14/12 = 1.17$; area $= 4$ inches $\times 1.5$ inches $= 6$ square inches

In order to determine coil current and voltage from Figure 16, the following procedure should be followed:

I. Current

Locate $H_o = 116$ (point A)
Draw AB (B is the same for all values of H_o)
Note point C and then locate $N/l = 1.17$ (point D)
Draw CD and locate point $E = 142$
$I = K_1 \times (\text{point } E) = 1.20 \times 142 = 171$ amperes

In order to apply the coil correction factor, l/d must be determined. As an approximation, d is taken as the so-called "effective diameter" of a rectangular coil, or that diameter which gives the same area as in the rectangular cross section.

Effective diameter:

$$d_{\text{eff}} = \sqrt{\frac{A\,(\text{rectangular})}{\pi/4}} = \sqrt{\frac{6 \times 4}{\pi}} = 2.76 \text{ inches}; \quad l/d = \frac{12}{2.76} = 4.35$$

$K_1 = 1.20$

J. Voltage

Locate $H_o = 116$ (point A)
Draw $A\,B'$ (B' is the same for all values of H_o)
Note point C' and then locate $N = 14$ (point D')
Draw $C'D'$ and note point $E' = 4.5 \times 10^{-4}$ (E/fak_2)
Locate $A = 6$ (point F') and draw $E'F'$
$\psi = E/fK_2 = 2.7 \times 10^{-3}$ (point G'); $dw/dc \cong 0$; $K_2 = 1$
$E = \psi K_2 f = 2.77 \times 10^{-3} \times 4.5 \times 10^{5} \times 1 = 1{,}250$ volts, coil voltage

K. Rate of heating $=$ pounds per minute $\times \Delta t = 15.4 \times (550-72) = 7{,}360$
From Figure 15, total power required $= 20{,}000$ watts

The curves in Figure 15 give the approximate power required to heat a given load plus the heater coil losses—a total of 20 kw in this example.

L. Kva/kw for load circuit

$$\frac{1.25 \times 171}{20} = 10.7$$

This value of kva/kw for the load circuit along with the 1,250 volts and 171 amperes are all reasonable figures for a 20-kw radio-frequency generator, and the coil assumption made to arrive at these figures is therefore entirely satisfactory.

EXAMPLE 2. NONMAGNETIC STRIP

Assume that a 0.0625-inch-thick strip of stainless steel 15 inches wide is to be heated to a temperature of 720 degrees Fahrenheit. This is to be done in order to heat the material for a toughening drawing operation with a quick oil quench to follow immediately after the heating operation. It is assumed the strip will be traveling at a continuous rate of 2.5 feet per minute.

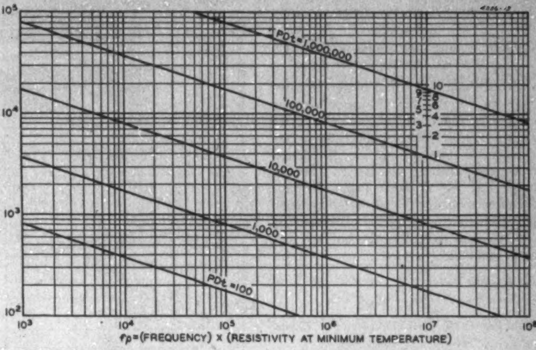


Figure 13. Magnetizing force required for magnetic strip

$$Wt = 1.37 \times 10^4 \sqrt{f\rho}\, H_o^{3/2}$$

W = power density in watts per cubic inch
t = strip thickness in inches
ρ = resistivity at minimum temperature in microhm-centimeters
f = frequency in cycles
H_o = peak magnetizing force in oersteds

$\rho = 20$ microhm-centimeters at minimum temperature

A. Volume to be heated $= 0.0625 \times 15 \times 2.5 \times 12 = 28$ cubic inches per minute

B. Weight heated per minute $= 28 \times 0.286 = 8.0$ pounds per minute

C. Thermal power $= 1.76 \times 10^{-2} \times 8.0 \times 0.12 \times (720-72) = 10.8$ kw

D. Volume of metal per minute in coil (assume $l = 12$ inches) $= 28/2.5 = 11.2$ cubic inches

E. Power density $= \dfrac{10{,}800}{11.2} = 965$ watts per cubic inch

F. $\rho f = 450{,}000 \times 20 = 9 \times 10^6$

G. From Figure 14, $\dfrac{PD}{H_o^2} = 3.9 \times 10^{-2}$ (by extrapolation)

$H_o^2 = 965/3.9 \times 10^{2} = 2.46 \times 10^4$; $H_o = 157$ oersteds

H. Assume a coil

$l = 12$ inches; $N = 12$; area $= 18$ inches $\times 1.5$ inches $= 27$ square inches

From Figure 16, $I/K_1 = 224$ · $I = 224 \times 1.3 = 291$ amperes coil current

$$D_{\text{eff}} = \sqrt{\frac{27 \times 4}{\pi}} = 5.86 \text{ inches}; \quad K_1 = 1.3; \quad \psi = 14.6 \times 10^{-3}$$

$dc/dw \cong 0$ $K_2 = 1$; $E = 14.6 \times 10^{-3} \times 4.5 \times 10^5 \times 1 = 6{,}600$ volts, coil voltage

I. Rate of heating $=$ pounds per minute $\times \Delta t = 8.0 \times (720-72) = 5{,}190$
From Figure 15, total power required $= 20{,}000$ watts

J. Load circuit kva/kw $= \dfrac{6.6 \times 291}{20} = 96$

This is beyond the limits of a standard 20-kw generator. Therefore, it is necessary to use a 50-kw generator operating at reduced power to provide the high kva/kw ratio necessary to satisfy this load condition.

EXAMPLE 3. SOLID MAGNETIC CYLINDER

Let us assume that, in order to anneal 0.25-inch steel wire before drawing it through a reducing die, it is required to heat the wire to a temperature of 1,000 degrees Fahrenheit. A continuous production rate of 40 feet per minute is required.

$\rho = 20$ microhm-centimeter at minimum temperature

A. Volume to be heated per minute $= \dfrac{\pi}{4} d^2 l = \dfrac{\pi}{4} \times (0.25)^2 \times 40 \times 12 = 23.5$ cubic inches per minute

B. Weight to be heated per minute $= 23.5 \times 0.286 = 6.74$ pounds per minute

C. Thermal power $= 1.76 \times 10^{-2} \times 6.74 \times 0.125 \times (1{,}000-72) = 13.7$ kw

D. Volume of metal in coil (assume $l = 12$ inches) $= 23.4/40 = 0.585$ cubic inch

E. Power density $= \dfrac{13{,}700}{0.585} = 23{,}400$ watts per cubic inch

F. $f\rho = 9 \times 10^6$; $PD \times d = 23{,}400 \times {}^1/_4 = 5{,}850$

G. From Figure 12, $H_o = 370$

H. Assume a coil

$l = 12$ inches; $N = 48$; $d = 0.75$ inch

From Figure 16, $I/K_1 = 132$; $K_1 = 1$; $I = 132$ amperes, coil current

$\psi = 2.24 \times 10^{-3}$; $\dfrac{dw}{dc} = \dfrac{0.25}{0.75} = 0.334$; $K_2 = 0.98$

$E = 2.24\times10^{-3}\times4.5\times10^{5}\times0.98 = 985$ volts, coil voltage

I. Rate of heating = pounds per minute × $\Delta t = 6.74\times928 = 6{,}250$

From Figure 15, total power required = 18 kw. A standard 20-kw rating generator is therefore necessary. From Figure 6, this rating of generator is capable of 175 amperes output. The aforementioned assumed coil design does not make full use of this output current; therefore a change in coil design is necessary to match properly the generator impedance and to utilize the full 175 amperes available.

J. Assume a coil

$l = 12$ inches; $N = 36$; $d = 0.75$ inch

From Figure 16, $\dfrac{I}{K_1} = 175$ $K_1 = 1$; $I = 175$ amperes, coil current

$\psi = 1.68\times10^{-3}$; $\dfrac{dw}{dc} = \dfrac{0.25}{0.75} = 0.334$; $K_2 = 0.98$;

$E = 1.68\times10^{-3}\times4.5\times10^{5}\times0.98 = 742$ volts

K. Load circuit kva/kw

$$\frac{0.742\times175}{18} = 7.2$$

The current, voltage, and kva/kw ratio of the load circuit now agree with the characteristics of a standard 20-kw generator, and we therefore have arrived at a suitable, work-circuit design.

Example 4. Solid Nonmagnetic Cylinder

We shall assume that it is desired to heat four-inch lengths of one-half-inch-diameter stainless-steel rod to a temperature of 1,600 degrees Fahrenheit in order to perform a forging operation. A production rate of 15 such ends per minute is desired, neglecting loading time. Induction heating is ideal for this application, since heating can be confined to the four-inch length at the end of the rod.

A. Volume to be heated per piece $= \pi/4\times(0.5)^2\times4 = 0.785$ cubic inch

B. Weight to be heated per piece $= 0.785\times0.288 = 0.226$ pound per piece

C. Rate of heating = 15 pieces per minute $= 0.226\times15 = 3.39$ pounds per minute

D. Thermal power $= 1.76\times10^{-2}\times3.39\times0.125\times(1{,}600-72) = 11.4$ kw

E. Power density $= \dfrac{11{,}400}{0.785} = 14{,}550$ watts per cubic inch

F. $\rho f = 450{,}000\times20 = 9\times10^{6}$

G. From Figure 14, $\dfrac{PD}{H_o^2} = 9.8\times10^{-3}$

$H_o^2 = \dfrac{14{,}550}{9.8}\times10^{3} = 1.48\times10^{6}$; $H_o = 1{,}220$ oersteds

This is a relatively high magnetizing force to be easily obtained from an oscillator.

In order to obtain more easily the required magnetizing force, three coils will be used in series, heating three separate rods simultaneously.

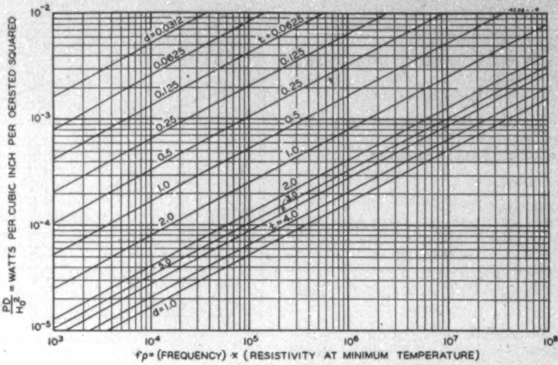


Figure 14. Magnetizing force required for nonmagnetic materials

$$\frac{W}{H_o^2} = \frac{1.63\times10^{-6}\sqrt{f\rho}}{d}$$

$$\frac{W}{H_o^2} = \frac{8.15\times10^{-7}\sqrt{f\rho}}{t}$$

W = power density in watts per cubic inch
H_o = peak magnetizing force im oersteds
f = frequency in cycles
ρ = resistivity at minimum temperature
d = diameter of cylinder in inches
t = thickness of strip in inches
$f\rho$ = (frequency) × (resistivity at minimum temperature)

H. Power density per coil $= \dfrac{14{,}550}{3} = 4{,}850$ watts per cubic inch

I. Magnetizing force per coil

$H_o^2 = \dfrac{4{,}850}{9.8}\times10^{3} = 49.5\times10^{4}$; $H_o = 705$ oersteds per coil

By the use of three coils, the magnetizing force per coil has been reduced. The rate of heating per coil will be one-third the original rate using one coil, but the over-all rate of heating will remain the same. Figure 15 is based upon a coil loss which is proportional to the thermal power supplied. To this approximation the coil losses will remain unchanged, as will the total power capacity required of the oscillator.

J. Assume a coil

$l = 4$ inches; $N = 23$; $N/l = 5.75$; $d = 0.75$ inch

From Figure 16, $\dfrac{I}{K_1} = 175$; $\dfrac{l}{d} = 5.33$; $K_1 = 1.0$

$I = 175\times1.0 = 175$ amperes; $\psi = 2.04\times10^{-3}$

$\dfrac{dw}{dc} = \dfrac{0.5}{0.75} = 0.665$; $K_2 = 0.77$

$E = 2.04\times10^{-3}\times0.77\times4.50\times10^{5} = 710$ volts per coil

Total voltage $= 710\times3 = 2{,}130$

K. Rate of heating $= 3.39\times(1{,}600-72) = 5{,}200$ = pounds per minute × Δt

From Figure 15, 20 kw will be required to supply useful power plus losses

L. Load circuit kva/kw $= \dfrac{2.130\times175}{20} = 18.6$

The voltage, current, and kva/kw ratio all satisfy the requirements of a standard 20-kw generator, and the load has been made to match the generator perfectly by proper selection of the number of work coils and the proper number of turns in each coil.

Example 5. Nonmagnetic Hollow Cylinder

Assume that it is desired to heat a one-inch-diameter hollow brass tube with 0.05-inch-wall thickness to a temperature of 322 degrees Fahrenheit, in order to flow an alloy coating which has been plated on the cylinder. A continuous rate of 25 feet per minute is required.

$\rho = 8$ microhm-centimeters (at minimum temperature)

A. Volume to be heated per foot $= \dfrac{\pi}{4}[(D_{O.D.})^2-(D_{I.D.})]l = \dfrac{\pi}{4}\left[1^2-0.9^2\right]\times12 = 1.80$ cubic inch per foot

B. Weight of metal per foot = volume per foot × weight per cubic inch $= 1.80\times0.32 = 0.575$ pounds per foot

C. Rate of heating $= 25\times0.575 = 14.4$ pounds per minute

D. Thermal power $= 1.76\times10^{-2}\times14.4\times0.08\times(322-72) = 5.05$ kw

E. Volume in coil of solid cylinder of same outside diameter (assuming a coil 12 inches long)

$V = \dfrac{\pi}{4}\times(1)^2\times12 = 9.44$ cubic inches

F. Power density $= \dfrac{5{,}050}{9.44} = 535$ watts per cubic inch

G. $f\rho = 8\times4.5\times10^{5} = 3.6\times10^{6}$

H. From Figure 14, $\dfrac{PD}{H_o^2} = 3.1\times10^{-3}$

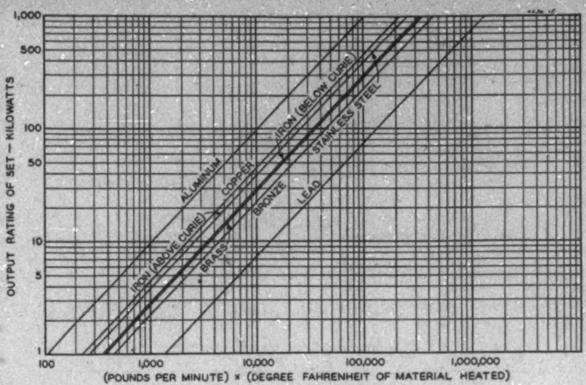


Figure 15. Oscillator power capacity for radio-frequency heating requirements

$$H_e^2=\frac{535}{3.1}\times 10^3=17.3\times 10^4$$

$$H_e=415 \text{ oersteds}$$

I. Assume a coil
$l=12$ inches; $d=1.25$ inches; $N=42$; $N/l=3.5$; $A=1.23$ square inches

From Figure 16, $\frac{I}{K_1}=170$; $l/d=9.6$; $K_1=1$;
$I=170$ amperes, coil current

$\psi=61.4\times 10^{-4}$; $\frac{dw}{dc}=\frac{1.0}{1.25}=0.8$; $K_2=0.62$

$E=61.4\times 10^{-4}\times 4.5\times 10^5\times 0.62=1{,}710$ volts, coil voltage

J. Rate of heating $=14.4\times(322-72)=3{,}600=$ pounds per minute $\times\,\Delta t$

From Figure 15, total power required $=10{,}000$ watts

K. The current of 170 amperes is beyond that obtainable from the usual ten-kilowatt oscillator. If the maximum possible current is assumed to be 110 amperes, the work coil must be partially tuned in order to obtain the benefit of high circulating currents.

$$I_c=I_L-I_t=170-110=60$$

$$\text{Capacitance required}=C=\frac{I_c}{\omega E}$$

$$=\frac{60}{2\pi\times 450{,}000\times 1{,}710}=0.0125 \text{ microfarad}$$

Kilovolt-amperes of capacitors $=1{,}710\times 60\times 10^{-3}=103$ kva

L. Work circuit kva/kw $=\frac{1{,}710\times 170}{10{,}000}=29.0$

M. Depth of penetration:

$$\delta=1.98\sqrt{\frac{\rho}{\mu f}}$$

Since δ is proportional to $\sqrt{\rho}$, the value of ρ at maximum temperature will be used in order to make certain that the depth of penetration is at all times less than the wall thickness. Refer to equation 24 in the appendix for an explanation of the formula for depth of penetration and the terms used therein.

Resistivity at maximum temperature:

$$\rho \text{ hot}=\rho \text{ cold }(1+\alpha\Delta t)$$

where

$\Delta t=$ temperature rise in degrees centigrade
$\alpha=$ temperature coefficient of resistivity $=0.003$ for brass

$$\mu=1$$

$$\rho \text{ hot}=8\left[1+\frac{0.003\times 250}{1.8}\right]=11.3 \text{ microhm-centimeters}$$

$$\delta=1.98\sqrt{\frac{11.3}{450{,}000}}=0.01 \text{ inch}$$

The frequency is therefore of a high enough value so that the depth of penetration is less than half the wall thickness, as was required.

Alternate solutions are often desirable from the economic viewpoint. This application offers such an alternate solution. The foregoing figures indicate a generator current of 170 amperes which is a value obtainable from a 20-kw generator without the aid of load capacitors or transformer. The power requirements were ten kilowatts, and therefore, by using two work circuits in series and a 20-kw generator, the production rate can be doubled, and no accessories to the generator are necessary. The current, voltage (1,710 × 2) and kva/kw ratio satisfy the characteristics of a 20-kw generator.

Example 6. Magnetic Hollow Cylinder

It will be assumed that, in order to temper-0.25-inch-diameter steel tubing with 0.025-inch wall thickness, it is necessary to heat this tubing to a temperature of 1,000 degrees Fahrenheit, while the tubing is moving at a continuous rate of 100 feet per minute.

$\rho=20$ microhm-centimeters (at minimum temperature)

A. Volume to be heated per foot $=\pi/4[0.25^2-0.20^2]\times 12=0.212$ cubic inch

B. Weight to be heated per foot $=0.212\times 0.288=0.061$ pound per foot

C. Rate of heating $=0.061\times 100=6.1$ pounds per minute

D. Thermal power $=1.76\times 10^{-3}\times 6.1\times 0.125\times(1{,}000-72)=12.4$ kw

E. Volume in coil of solid cylinder of same outside diameter (assuming a coil 12 inches long):

$$V=\frac{\pi}{4}\times(1/4)^2\times 12=0.59 \text{ cubic inch}$$

F. Power density $=\frac{12{,}400}{0.59}=21{,}000$ watts per cubic inch

G. $f\rho=4.5\times 10^5\times 20=9\times 10^6$; $PD\times d=21{,}000\times 0.25=5.25\times 10^3$

H. From Figure 12, $H_e=344$ oersteds

I. Assume a coil
$=12$ inches; $d=0.8$ inch; $N=36$; $N/l=3$; $A=0.5$ square inch

From Figure 16:
$l/d=15$; $K_1=1$; $I/K_1=164$; $I=164$ amperes, coil current

$\psi=1.80\times 10^{-3}$; $\frac{dw}{dc}=\frac{0.25}{0.8}=0.313$; $K_2=1$;

$E=1.80\times 10^{-3}\times 4.5\times 10^5\times 1=810$ volts, coil voltage

J. Rate of heating $=6.1\times(1{,}000-72)=5{,}650=$ pounds per minute $\times\,\Delta t$

From Figure 15, total power required $=16$ kw

K. Work circuit kva/kw $=\frac{810\times 164}{16{,}000}=8.3$

L. Depth of penetration:

$$\delta=1.98\sqrt{\frac{\rho}{\mu f}}$$

$$\rho \text{ hot}=20\left[1+\frac{0.003\times 928}{1.8}\right]=50.6$$

$$\mu=\frac{28{,}500}{344}=83.0$$

$$\delta=1.98\sqrt{\frac{50.6}{450{,}000\mu}}$$

$$\delta=1.98\sqrt{\frac{50.6}{450{,}000\times 83}}=2.32\times 10^{-3} \text{ inches}$$

The depth of penetration is much less than the required half the wall thickness. Equations 30 and 37 in the appendix give the necessary explanation for the formulas for permeability and depth of penetration, respectively.

The foregoing figures satisfy the requirements of a 20-kw generator, but they do not provide for full use of the power available from this 20-kw unit. It would be desirable, therefore, to select a coil design to utilize the full rated current of 175 amperes and power of 20 kw from a standard generator. The rate of production (100 feet per minute) thereby can be increased by the ratio 20/16 or to 125 feet per minute. Recalculation on this basis will give a coil design capable of fully loading the standard 20-kw generator.

Example 7. Case Hardening

In this example we shall assume that a steel bearing 0.5 inch in diameter and one inch long is to be heated to a surface temperature of 1,600 degrees Fahrenheit in order to case-harden to a depth of 0.03 inch.

A. Area to be hardened $= \pi dl = \pi \times 0.5 \times 1 = 1.57$ square inches

B. Power required $=2$ kw per square inch $= 1.57 \times 2 = 3.14$ kw

C. Volume of metal in coil $= \pi/4 \times (0.5)^2 \times 1 = 0.196$ cubic inch

D. Power density $= \dfrac{3{,}140}{0.196} = 16{,}000$ watts per cubic inch

E. $H_o = 720$ (from analysis in appendix, equation 50)

F. Assume a coil

$N=2$; $l=1$ inch; $N/l=2$; $d=0.75$ inch; $A=\pi/4 \times 0.75^2 = 0.44$ square inch

From Figure 16

$\dfrac{I}{K_1} = 514$; $l/d = \dfrac{1}{0.75} = 1.34$; $K_1 = 1.35$

$I = 1.35 \times 514 = 694$ amperes, coil current; $\psi = 1.81 \times 10^{-4}$

$\dfrac{dw}{dc} = \dfrac{0.5}{0.75} = 0.665$; $K_2 = 0.78$; $E = 1.81 \times 10^{-4} \times 4.5 \times 10^5 \times 0.78 = 63.5$ volts, coil voltage

G. Power loss in work coil (refer to equation 9):

$$W_c = 1.40 \frac{dc}{dw} \sqrt{\frac{\rho c}{\rho w}}\, W_w \; ; \quad W_c = 1.40 \times \frac{0.75}{0.5} \sqrt{\frac{2.1}{20}} \times 3.14 = 2.12 \text{ kw}$$

Total power required $= 3.14 + 2.12 = 5.26$ kw

H. Load circuit kva/kw $= \dfrac{63.5 \times 694}{5{,}260} = 8.3$

I. Depth of penetration

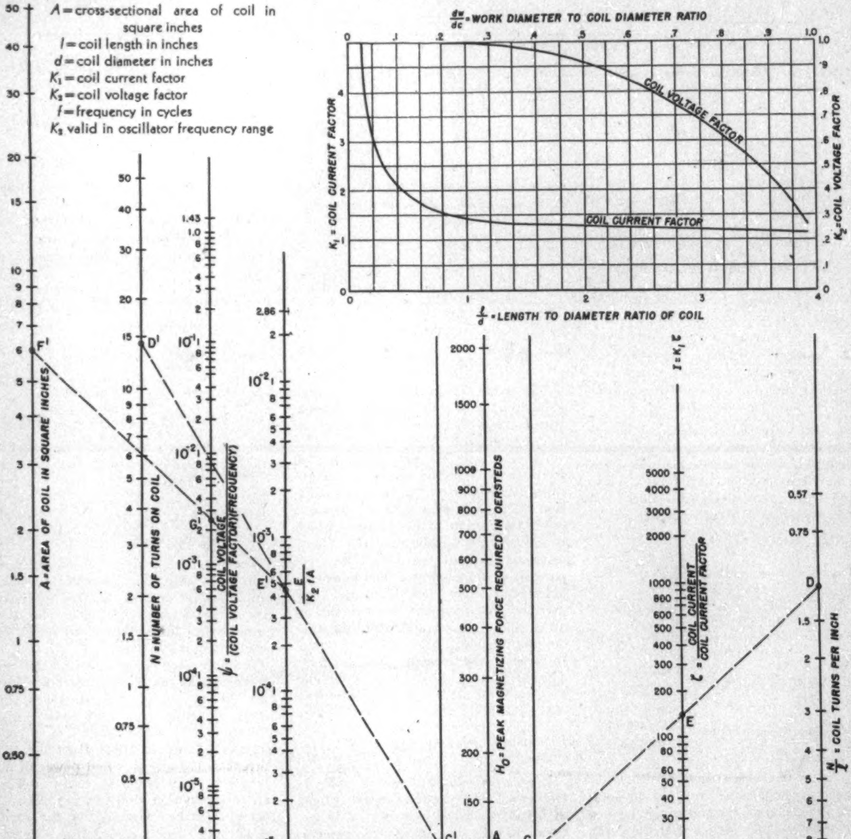
$$\delta = 1.98 \sqrt{\frac{\rho}{\mu f}} \qquad \mu = \frac{28{,}500}{H_o} = \frac{28{,}500}{720} = 39.5$$

Figure 16. Voltage and current of a solenoid as a function of peak magnetizing force

$E = H_o(2.86 \times 10^{-7} NA/lK_2)$

$I = H_o\left(1.43 \dfrac{l}{N}\right)(K_1)$

E = coil voltage in volts rms
I = coil current in amperes rms
H_o = peak magnetizing force in oersteds
N = number of turns in coil
A = cross-sectional area of coil in square inches
l = coil length in inches
d = coil diameter in inches
K_1 = coil current factor
K_2 = coil voltage factor
f = frequency in cycles
K_2 valid in oscillator frequency range



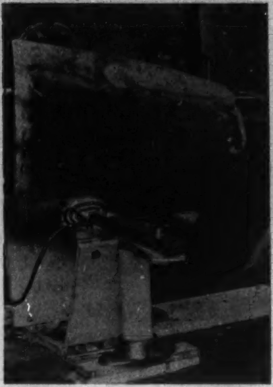


Figure 17. Propeller-edge brazing at 450 kilocycles

Equations 30 and 37 in the appendix give the necessary explanation for the formulas for permeability and depth of penetration, respectively.

$$\rho \text{ hot} = 20\frac{(1+0.003\times 1{,}528)}{1.8} = 71$$

$$\delta = 1.98\sqrt{\frac{71}{39.5\times 450{,}000}} = 3.96\times 10^{-3} \text{ inches}$$

The frequency used is therefore high enough so that the depth of penetration is less than half the depth of case desired.

The coil current is considerably beyond a value practically obtainable from an oscillator rated at five or ten kilowatt output. The load-circuit kva/kw ratio and the current requirements indicate that a current transformer is an ideal solution.

The total power requirements given in G do not include transformer losses. To accomplish this heating job, it is therefore necessary to use a ten-kilowatt generator to provide the relatively high current-transformer losses in addition to work-circuit power. There will be a margin of safety if a ten-kilowatt generator is employed which by proper design of the work circuit can be used to increase the kilowatt per square inch into the work and thereby reduce the heating time.

Appendix

The theoretical basis from which the equations used were derived, together with the derivation of these equations, will be treated briefly in this section.

When any conductor is placed in a varying magnetic field, currents are induced in the conductor as in a transformer secondary winding. Since the conductor has definite resistivity, heat is generated by the induced currents. This principle is known as induction heating. The correct frequency to be used depends upon the size and electrical properties of the piece to be heated. This frequency may vary from 60 cycles or less for large objects to several hundred kilocycles for case hardening and heating of small objects.

In general, magnetic properties make iron and steel below the Curie point, much easier to heat than the other metals. The Curie point, approximately 1,300 degrees Fahrenheit, is that temperature above which iron loses its magnetic properties. Materials of low resistivity, such as aluminum and copper, are more difficult to heat than the poorer conductors.

Nearly any problem in the induction-heating field can be resolved into that of heating either a cylinder or strip of magnetic or nonmagnetic material. If a hollow cylinder is to be heated, satisfactory results require that the depth of current penetration caused by "skin effect" be less than the wall thickness of the cylinder, which sets a lower limit to the frequency.

It has been shown[2] that power loss in a cylinder may be represented as:

$$P = \frac{(H_0')^2 \rho m}{0.08\pi 2a} \times \left\{\frac{\text{ber}\,(ma)\,\text{ber}'\,(ma) + \text{bei}\,(ma)\,\text{bei}'\,(ma)}{\text{ber}^2\,(ma) + \text{bei}^2\,(ma)}\right\} \text{ watts per cubic centimeter} \quad (16)$$

where

$$ma = a\sqrt{\frac{8\pi^2 \mu f \times 10^{-9}}{\rho}} \quad (17)$$

f = frequency (cycles per second)
ρ = resistivity (ohm-centimeters)
a = radius of cylinder (centimeters)
μ = permeability

Let

$$G(ma) = \frac{1}{ma} \times \frac{\text{ber}\,(ma)\,\text{ber}'\,(ma) + \text{bei}\,(ma)\,\text{bei}'\,(ma)}{\text{ber}^2\,(ma) + \text{bei}^2\,(ma)} \quad (18)$$

Then

$$P = \frac{(H_0')^2 \rho m^2}{0.08\pi^2}\,\frac{1}{ma} \times \frac{\text{ber}\,(ma)\,\text{ber}'\,(ma) + \text{bei}\,(ma)\,\text{bei}'\,(ma)}{\text{ber}^2\,(ma) + \text{bei}^2\,(ma)}$$

$$= (H_0')^2 \rho\,\frac{(8\pi^2 \mu f \times 10^{-9})}{0.08\pi^2 \rho}\,G(ma)$$

$$= \mu (H_0')^2 f G(ma) \times 10^{-7} \text{ watts per cubic centimeter} \quad (19)$$

where

H_0' = rms magnetizing force

If

H_0 = peak magnetizing force

then

$$H_0 = \sqrt{2} H_0'$$

and

$$P = {}^1\!/_2 \mu H_0^2 f G(ma) \times 10^{-7} \text{ watts per cubic centimeter} \quad (20)$$

Power density will be maximum for a given frequency when the function $G(ma)$ is maximum. This occurs with a value of $(ma) \leqq 3$. Operation is unstable with values of (ma) less than 3, and the minimum frequency is taken to be that frequency which makes $(ma) = 3$. This frequency is not critical and any frequency for which $(ma) \geqq 3$, will be satisfactory, providing current and voltage limitations in the work coil are not exceeded. An increased frequency lowers the power factor of the coil, but increases the power-input-to-magnetizing-force ratio. The highest possible ratio is desired without exceeding current or voltage limitations. The function $G(ma)$ may be replaced with very little inaccuracy when

$(ma) \geqq 10$ by

$$G(ma) = \frac{1}{ma\sqrt{2}} \quad (21)$$

and for practical purposes when $(ma) \geqq 3$. Power density therefore simplifies to

$$P = \frac{1}{2ma\sqrt{2}} \mu H_0^2 f \times 10^{-7} \text{ watts per cubic centimeter} \quad (22)$$

For a coil long with respect to its diameter,

$$H_0 = \frac{0.4\pi NI}{l}\sqrt{2} \text{ oersteds} \quad (23)$$

where NI/l = ampere turns per centimeter of the coil. If the diameter of the coil is comparable to its length, the value given above for H_0 is modified by the factor K_1, plotted in Figure 14.

The same relationships are valid for a hollow cylinder, providing the depth of penetration of current is less than the wall thickness.

Effective depth of current penetration is given as

$$\delta = 5{,}033\sqrt{\frac{\rho}{\mu f}} \text{ centimeters} \quad (24)$$

where ρ = resistivity in ohm-centimeters. The minimum frequency for hollow cylinder

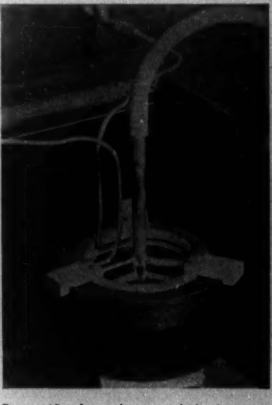


Figure 18. Internal-gear-tooth hardening at 450 kilocycles

has been taken arbitrarily as twice the frequency which would produce a depth of penetration equal to the wall thickness.

The power input to a flat strip is given as

$$P = {}^1/_2\mu H_o^2 f G(K_s t)\times 10^{-7} \text{ watts per cubic centimeter} \quad (25)$$

where

$$K_s t = t\sqrt{\frac{4\pi^2\mu f\times 10^{-9}}{\rho}} \quad (26)$$

t = thickness of strip (centimeters)
ρ = resistivity (ohm-centimeters)

$$G(K_s t) = \frac{1}{2K_s t}\,\frac{\sinh\,(K_s t) - \sin\,(K_s t)}{\cosh\,(K_s t) + \cos\,(K_s t)} \quad (27)$$

The function, $G(K_s t)$ has a maximum value of $K_s t \cong 3$, as in the case of the cylinder. For practical purposes when

$$K_s t \geqq 3 \quad (28)$$

$G(K_s t)$ may be replaced by $1/2K_s t$. In this case, the power-input equation reduces to

$$P = \frac{1}{4K_s t}\,\mu H_o^2 f\times 10^{-7} \text{ watts per cubic centimeter} \quad (29)$$

These equations are valid for magnetic materials, provided that μ remains constant or varies according to some mathematical relationship. Since μ is ordinarily not constant and does not have a regular variation, approximation is necessary. If a constant value of μ, based on empirical and theoretical considerations and equal to

$$1.78\sqrt{\frac{Bm}{H_o}} \quad (30)$$

is used, where Bm is the saturation flux density of iron and is approximately equal to 16,000 gausses, the equation will give reasonably accurate results, for both strip and cylinder.

Equations 2 through 7 and 10 through 14 are derived as follows: General equation for power input to a cylinder

$$PD = {}^1/_2\mu H_o^2 G(ma) f\times 10^{-7} \text{ watts per cubic centimeter}$$
$$= 8.2\times 10^{-7}\mu f G(ma) H_o^2 \text{ watts per cubic inch} \quad (31)$$

$$ma = a\sqrt{\frac{8\pi^2\mu f\times 10^{-9}}{\rho}}$$
$$= 0.356\sqrt{\frac{\mu f}{\rho}}\,d \quad (32)$$

where

d = diameter of cylinder in inches
ρ = resistivity in microhm-centimeters

Let

$$G(ma) = \frac{1}{ma\sqrt{2}}$$

$$PD = \frac{8.2H_o^2\mu f\sqrt{\rho}\times 10^{-7}}{\sqrt{2}\times 0.356d\sqrt{\mu f}}$$
$$= \frac{16.3H_o^2}{d}\sqrt{\mu f\rho}\times 10^{-7} \text{ watts per cubic inch} \quad (33)$$

Power input to a magnetic cylinder:

$$\mu = \frac{1.78\times 16{,}000}{H_o} = \frac{28{,}500}{H_o}$$

$$PD = \frac{16.3}{d}\sqrt{\rho f}\sqrt{\frac{28{,}500}{H_o}}\,H_o^2\times 10^{-7}$$
$$= \frac{2.75\times 10^{-4}}{d}\sqrt{\rho f}\,H_o^{3/2} \quad (2)$$

Power input to a nonmagnetic cylinder ($\mu = 1$):

$$PD = \frac{1.63}{d}\sqrt{\rho f}\,H_o^2\times 10^{-6} \quad (4)$$

General equation for power input to a strip:

$$PD = {}^1/_2\mu H_o^2 f G(K_s t)\times 10^{-7} \text{ watts per cubic centimeter}$$
$$= 8.2\times 10^{-7}\mu f G(K_s t) H_o^2 \text{ watts per cubic inch} \quad (34)$$

$$K_s t = t\sqrt{\frac{4(\pi)^2\mu f\times 10^{-9}}{\rho}}$$
$$= 0.505t\sqrt{\frac{\mu f}{\rho}} \quad (35)$$

where

t = strip thickness in inches
ρ = resistivity in microhm-centimeters

Let

$$G(K_s t) = \frac{1}{2K_s t}$$

$$PD = \frac{8.2H_o^2\mu f\sqrt{\rho}\times 10^{-7}}{2\times 0.505t\sqrt{\mu f}}$$
$$= \frac{8.15H_o^2\sqrt{\mu f\rho}\times 10^{-7}}{t} \text{ watts per cubic inch} \quad (36)$$

Power input to a magnetic strip:

$$\mu = \frac{28{,}500}{H_o}$$

$$PD = \frac{8.15}{t}\sqrt{f\rho}\sqrt{\frac{28{,}500}{H_o}}\,H_o^2\times 10^{-7}$$
$$= \frac{1.37\times 10^{-4}\sqrt{f\rho}\,H_o^{3/2}}{t} \text{ watts per cubic inch} \quad (3)$$

Power input to a nonmagnetic strip ($\mu = 1$):

$$PD = \frac{8.15\times 10^{-7}\sqrt{f\rho}\,H_o^2}{t} \text{ watts per cubic inch} \quad (5)$$

Minimum frequency for heating a solid cylinder:

$$ma = 0.356d\sqrt{\frac{\mu f}{\rho}}$$

Let

$$ma = 3$$

then

$$0.356d\sqrt{\frac{\mu f}{\rho}} = 3$$

$$\mu f = \frac{71\rho}{d^2} \quad (10)$$

Minimum frequency for heating a strip:

$$K_s t = 0.505t\sqrt{\frac{\mu f}{\rho}}$$

Let

$$K_s t = 3$$

then

$$0.505t\sqrt{\frac{\mu f}{\rho}} = 3$$

$$\mu f = 35.2\,\frac{\rho}{t^2} \quad (13)$$

Magnetic permeability at minimum frequency for steel cylinder or strip below Curie point:

$$PD = 8.2\times 10^{-7}\mu f G(ma) H_o^2$$
$$= 8.2\times 10^{-7}\mu f G(K_s t) H_o^2$$

At minimum frequency

$$ma = 3$$
$$K_s t = 3$$
$$G(ma) = G(K_s t) = 0.186$$
$$PD = 1.52\times 10^{-7}\mu f\,H_o^2$$

$$\mu = \frac{28{,}500}{H_o} \qquad H_o = \frac{28{,}500}{\mu}$$

$$PD = 1.52\times 10^{-7}(\mu f)\times\left(\frac{28{,}500}{\mu}\right)^2$$

$$\mu = \sqrt{\frac{123}{PD}\,\mu f} \quad (14)$$

Minimum frequency for heating a hollow magnetic cylinder:

$$\delta = 5{,}033\sqrt{\frac{\rho}{\mu f}} \text{ centimeters}$$
$$= 1.98\sqrt{\frac{\rho}{\mu f}} \text{ inches} \quad (37)$$

where

ρ = resistivity in microhm-centimeters

$$\mu f = \frac{1.98^2}{\delta^2}\,\rho$$

Let

$\delta = t$ = wall thickness of cylinder

Let

$$f^1 = 2f \quad (38)$$

$$f^1 = \frac{1.98^2\times 2\rho}{\mu t^2};\quad \mu = \frac{28{,}500}{H_o}$$
$$= \frac{1.98^2\times 2\rho H_o}{28{,}500t^2}$$

$$PD = \frac{2.75\times 10^{-4}\sqrt{f'\rho}\,H_o^{3/2}}{d}$$

$$H_o = \left[\frac{3.64PD\times d\times 10^3}{\sqrt{f'\rho}}\right]^{2/3} \quad (2)$$

$$f' = \frac{1.98^2\times 2\rho}{28{,}500t^2}\left[\frac{3.64PD\times d\times 10^3}{\sqrt{f'\rho}}\right]^{2/3}$$

$$f^1 = \frac{1.98^2\times 2\rho}{28{,}500t^2}\left[\frac{PD\times d\times 10^{+4}}{2.75\sqrt{f'\rho}}\right]^{2/3}$$

$$f^1 = 0.129\left[\frac{PD\times d\times\rho}{t^3}\right]^{1/2} \quad (11)$$

Minimum frequency for heating a hollow nonmagnetic cylinder:

$$\delta = 1.98\sqrt{\frac{\rho}{f}}$$

Let

$$\delta = t$$
$$f^1 = 2f$$

$$t^2 = 1.98^2\frac{2\rho}{2f} = 1.98^2\frac{2\rho}{f'}$$

$$f^1 = \frac{7.75\rho}{t^2} \tag{12}$$

Magnetizing force for case hardening of a cylinder:
Let

E_1 = energy input to work while it is magnetic
E_2 = energy input to work when it passes the Curie point
E_0 = total energy input to the work
P_1 = power input in watts while magnetic
P_2 = power input in watts when nonmagnetic
t_1 = time material is magnetic
t_2 = time material is nonmagnetic

$$PD = \frac{K_1H_o^2\sqrt{\mu f\rho}}{d} \text{ watts per cubic inch}$$

$$\frac{P}{A} = K_2H_o^2\sqrt{\mu f\rho} \text{ watts per square inch of surface area}$$

$$\frac{P_1}{\pi dl} = K_2H_o^2\sqrt{\mu f\rho}$$

$$\frac{P_2}{\pi dl} = K_2 \qquad H_o^2\sqrt{f\rho} \ (\mu = 1)$$

$$\frac{P_1}{P_2} = \sqrt{\mu} \ (\text{for } H_{o1} = H_{o2}) \tag{39}$$

$$E_1 = \frac{13}{16}E_0 \tag{40}$$

$$E_2 = \frac{3}{16}E_0 \tag{41}$$

$$E_0 = 2(t_1+t_2)\pi dl \ (2 \text{ kw per square inch} \times \text{area} \times \text{time}) \tag{42}$$

$$E_1 = P_1t_1 = \frac{13}{16}\times 2(t_1+t_2)\pi dl \tag{43}$$

$$E_2 = P_2t_2 = \frac{3}{16}\times 2(t_1+t_2)\pi dl \tag{44}$$

$$P_2 = P_1/\sqrt{\mu}$$

If we solve equations 39, 43, and 44,

$$\frac{t_2}{t_1} = \frac{3}{13}\sqrt{\mu} \tag{45}$$

$$P_1t_1 + P_2t_2 = P_{eff}(t_1+t_2) \tag{46}$$

$$P_1t_1 + \frac{P_1}{\sqrt{\mu}}\times\frac{3}{13}\sqrt{\mu}\,t_1 = 2(t_1+t_2)\pi dl \tag{47}$$

$$P_1 = 1.63\pi dl\left(\frac{3}{13}\sqrt{\mu}+1\right) \tag{48}$$

$$\frac{P_1}{\text{Volume}} = PD = \frac{2.75\times10^{-7}\sqrt{f\rho}H_o^{3/2}}{d} = \frac{4P_1}{\pi d^2l} \tag{49}$$

$$4\times\left[\frac{1.63\pi dl(3/13\sqrt{\mu}+1)}{\pi d^2l}\right] = \frac{2.75\times10^{-4}\sqrt{f\rho}H_o^{3/2}}{d}$$

Let

$$f = 450 \text{ kilocycles}$$
$$\rho = 20 \text{ microhm-centimeters}$$
$$\sqrt{f\rho} = 3\times10^3$$
$$H_o^2 - 7.9\times10^3\sqrt{H} - 3.08\times10^6 = 0$$
$$H_o = 720 \tag{50}$$

Coil voltage and current as a function of magnetizing force:

$$L = \frac{N\phi}{1}\times10^{-8} = \frac{NHA}{I}\times10^{-8} \tag{51}$$

$$E = \omega LI = \omega NH_oA\times10^{-8}$$

where H_o = peak magnetizing force in oersteds

$$\omega = 2\pi f$$

$$E = \frac{H_o}{\sqrt{2}}\times(2\pi fNA\times10^{-8}), \text{ rms volts, for an air-core solenoid}$$

where A = area in square centimeters

$$E = H_o(2.86\times10^{-7}fNAK_2) \tag{6}$$

where E = rms volts for coil with a metal core

A = area of coil in square inches

$$I = H_o\left(\frac{l}{0.4\pi N}K_1\right)$$

where l = length of coil in centimeters

I = peak current in amperes

$$I = H_o\frac{2.54\,l}{0.4\pi\sqrt{2}N}K_1 = H_o\times\left(1.43\frac{l}{N}K_1\right) \tag{7}$$

where

I = rms current in amperes
l = length of coil in inches

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Electrically Heated Clothing

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NONMEMBER AIEE

THE efficiency of our air forces can be reduced very materially by physical discomfort[1] especially from the effects of severe cold. To be able to aim and fire machine guns and to operate bombsights and navigation instruments calls for finger dexterity and a mind and body not numbed by the cold found at high altitudes. Tests and experience have demonstrated that heated clothing will keep an inactive man comfortably warm for many hours at subzero temperatures. Such clothing was used to a small extent in the first World War and is used by all belligerents in this war. While many of our military craft have heating systems utilizing either engine heat or combustion heaters, not all parts of the ship can be heated, especially exposed positions where gunners are located. For these men, and for all the crew because of possible failure of the airplane heating system, electrically heated clothing is an important item in aerial warfare wherever subfreezing temperatures exist.

In the war now being fought, the airplane has changed the whole pattern of warfare. Our allied superiority in the air will shorten the war and save thousands or even millions of lives, not only of our own men but of the enemy as well by destroying his means to fight. The success of our air forces is due not only to our airmen and the men and women who design and build the airplanes, but also to a small army of scientists and engineers who in laboratories throughout the world have been studying the physiological aspects of flight including the effects of high altitude and intense cold upon the human body. As a result of these investigations our flyers are able to fly at increasingly greater heights where there is less danger from enemy interception and gunfire from the ground.

The chief factors involved in high-altitude flight from the standpoint of personnel are a supply of oxygen for the reduced atmospheric pressure and a source of heat for the extreme cold. At a height of six miles above the earth's surface the temperature often reaches 60 or more degrees below zero Fahrenheit even at the equator. To combat this cold, many of our flyers wear clothing heated electrically by current from the airplanes' generators. Thus fortified, they keep comparatively warm and comfortable on the longest bombing missions.

History

The use of electrically heated clothing by aviators is not new. Up to the first World War there was little need for it since the airplanes themselves could be flown only in good weather and at low altitude. A pilot required about the same clothing as a motorcyclist.

During the first World War the Edison Electric Appliance Company designed and built under contract for the War Department about 5,000 electrically heated flying suits. One of the author's associates, who was closely connected with this design and manufacture, has described the suit and its first test. Another associate, who was a pilot in World War I, claims to have had one of these suits issued to him in France and to have worn it on a flight to an altitude of 24,000 feet which was close to a record at that time.

The suit was two piece. The trousers had the feet in them like an Eskimo parka complete with hood and fur ruff and was pulled on over the head in the same manner. The suit was completely lined with fur and the outer covering was an olive-drab-colored woolen cloth of medium weight. Between the two layers were the heating inserts consisting of cotton strips about two inches wide with a channel down the center carrying the heating wires. These inserts extended down the arms and legs and the front and back of the trunk. One circuit ran up the back of the neck and over the top of the head to the forehead. This heating of the head is not considered necessary at the present time. (The head except for the ears and face is relatively insensitive to the cold.) The circuit was supplied from the six-volt battery in the airplane, but the current is not known, and whether any control other than a switch was provided is doubtful. To determine the effectiveness of the suit, an open car was obtained and provided with suit connections, and early on a cold Sunday morning in February 1918 two men wearing civilian clothes and two wearing the new heated suits got into the car and sped for several miles across Chicago. This simple test may have been as useful for the conditions of actual use at that time, as our present cold rooms and high-altitude chambers are for present flight conditions.

During the 20 years between wars there is little record of experiments with electrically heated clothing. Polar explorers, because of possible failures and forced landings while flying over arctic regions wore fur parkas of the type worn by the Eskimo. In 1925 the Western Air Express started operating an air-mail line between San Francisco and Salt Lake City.[2] Open-cockpit airplanes were used, and for a time the pilots reportedly wore electrically heated suits of the 1918 production. Another reference to such clothing is in the list of items carried by the Lindbergs on their flight to the Orient. Two electrically heated flying suits appear there, although no reference is made to their use.

Before proceeding to a discussion of present flying-suit construction, it may be of interest to compare the various methods available for heating military aircraft.

The first obvious method is to use some of the enormous amount of the heat wasted from the engine exhaust. In its elementary form this is a cover over the exhaust pipe to produce an exhaust gas-to-air heat exchanger such as was used in the early days of the automobile. Its chief advantages are that it is cheap and there is little to go wrong with it mechanically. Its chief disadvantage is that there is always danger of carbon monoxide poisoning, especially if a bullet passes through the exchanger. With a few exceptions this has banned its use in the past on airplanes. Also, on many liquid-cooled engines the exhaust discharges from each cylinder, and no manifold is available for such a heating system.

The second method is to use a boiler in or around the exhaust stack with a fluid heat-transfer medium such as ethylene glycol piped to a convection radiator in the cabin through which ventilation air from an air scoop or from a recirculating air blower is forced to pass. By a combination of dampers both air temperature and ventilation air volume can be controlled. This type of heater is in general use by the air lines and is quite satisfactory for flying at low altitude and moderate temperatures if kept properly serviced. In military aircraft it constitutes another point of vulnerability in that a bullet through any part of the system would put the heater out of commission because of loss of fluid.

The third method is to use combustion heaters which utilize aviation fuel burned in a small combustion chamber with the heat transferred to air blown over the heating surface. These are of two general types, one drawing atomized fuel from the engine manifold into its combustion chamber while the other has its own carburetor. These heaters are quite effective at low altitudes but involve a starting problem the same as the engines in regions of low ground temperature.

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There is also the problem of carrying not only the weight of the heater and such piping and ductwork as is necessary but also the weight of the fuel which will be consumed for heating. The added weight means less pay load or bomb load. Also, there is again the danger of possible leakage and monoxide poisoning, although, in general, the heated air may be kept at a pressure above that of the combustion products. At high altitudes the heater requires supercharging the same as the engines to maintain proper combustion ratios.

All of these types of heaters which involve the transfer of heat to air become relatively less effective as the air density decreases. The specific heat of air, which is a measure of its capacity to absorb heat, decreases with its density. Hence, increasingly larger volumes must be passed across the heated surfaces to transfer the heat from source to receiver. It is a case where the heater itself gets increasingly hotter while the airplane interior gets colder. Obviously on airplanes having supercharged pressure cabins these effects of altitude can be omitted. In fact, the heat of compression of the supercharger will supply all the heat necessary under certain conditions, depending upon ratio of compression, outside temperature, cabin insulation, and so forth.[3]

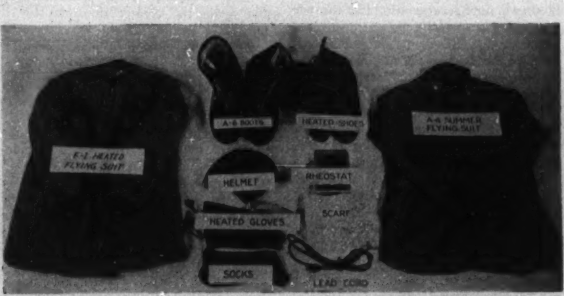


Figure 1A. Type F-1 heated flying suit and accessory clothing

The fourth method is the application of electric heat directly to the individual by passing a controlled quantity of current from the airplane's generators through resistance wires sewn in the linings of the clothing, shoes, and gloves. The following advantages are obvious:

1. Heat is individually controllable, so that each man can adjust his own supply to suit his particular activity, location, amount of extra clothing, and so forth. For example, a turret gunner with cold air blowing through cracks or gun slots will require more heat than someone in a more sheltered location.
2. There is no danger from carbon monoxide.
3. The added weight of the heated clothing over regular clothing, plus the added weight of generators, wiring, and rheostats, is less than any other type of heat with the possible exception of an exhaust heater.
4. Electrically heated clothing becomes more effective at high altitude, since the heat loss to the cabin air is reduced and therefore less electric energy is required.
5. Such clothing is less vulnerable to enemy fire since each suit has its own power-supply line.
6. Certain exposed positions such as waist and tail gunners have not been successfully heated by any other means.

Electrically Heated Suit Development

In 1940 engineers of the General Electric Company, who had pioneered the electrically heated blanket, anticipated that heated clothing would be necessary in this aerial war. In July of that year a meeting was arranged with the Wright Field clothing unit and it was found that they were looking at the same time for some organization to develop such a suit. The requirements were for a suit which would keep a flyer warm at any temperature from plus 70 to minus 60 degrees Fahrenheit. A proposed suit design was worked out and taken back to the factory. There materials were procured; a sewing machine of the proper type obtained; wire of the proper weight, resistance, and flexibility made up and sewed in place, and the first sample delivered 12 days later. It was an adaptation of the standard summer flying suit into which wires were sewn in parallel lines. Fine leather gloves, wired on the back and at the wrists, plugged into the sleeve of the garment. These gloves were made of a material and design commonly used in ladies' dress gloves. This construction assured a good tactile sense and permitted wearers to operate gun triggers, camera shutters, navigation instruments, and other devices used on airplanes which require a good sense of touch.

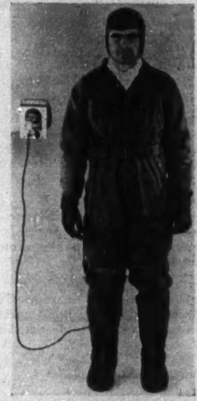


Figure 1B. Man outfitted with the same equipment

In this initial model boots were heated by means of a boot insert shaped like a clover leaf. Wires were cemented between the two layers of this insert, and a connecting cord was provided for plugging into receptacles in the legs of the suit. When this clover-leaf element was inserted into a standard intermediate flying boot, the center leaf became a heated insole for the boot and the other two leaves folded over the ankles.

Electrically the complete assembly consisted of two circuits for the suit, one circuit for gloves, and one circuit for shoe inserts. The four circuits were connected to a switch box at the waist, which permitted independent use of heat in shoes, gloves, or suit. From the switch box a short pigtail connection provided a means of connecting the assembly to the control box, which in turn was mounted on the ship and connected with the source of power.

When this suit was submitted to Wright Field, they were interested in its fundamental design and verbally placed an order for 15 suits incorporating improvements which they suggested. This order requested delivery within six weeks, inasmuch as some of the suits were to be taken on a flight of bombers scheduled for experimental cold-weather tests in Alaska.

The new suits were to be used in place of underclothing with a uniform and a summer flying suit over them. In this way with all of the clothing outside, the maximum heat utilization could be obtained. Heavy woolen union suits were selected for the garment to be heated, because knitted wool is elastic and a good thermal insulator. The wires were sewed in parallel waves to give an accordian effect which would stretch

Figure 2. A section of the factory where the electrically heated suits are manufactured

with the wool to conform to body movements with the minimum stress on the wires. The switch box was eliminated and the pigtail connection improved to permit a quick disconnect in the event the wearer had to bail out of the airplane. The finished suits then were tested in three cold rooms, having temperatures respectively of 35 degrees, zero and minus 50 degrees Fahrenheit with final test at minus 63 degrees Fahrenheit in the cold room at Fort Monmouth. In this extreme cold a face mask and goggles were added so that no portion of the test subjects' skin was exposed to the air. Although the tests were considered satisfactory for the initial model and the suits delivered on schedule, development work was continued for further improvements.

During this development, other suits were studied. An electrically heated suit obtained from a German aviator who had been shot down over Britain was examined thoroughly and analyzed for materials used, method of construction, and electrical characteristics. It was found to be expertly fabricated but very poor electrically. The materials were not flameproofed, and the suit produced so little warmth as to be almost worthless. A British suit also was examined. In this the heating wires were made of strips of stainless-steel wire mesh similar to window screen. These strips were insulated by a layer of cloth on each side, and they ran up and down the front and back of the suits and the arms and legs. Cloth shoe inserts with similar heating elements were worn inside of flying boots. A heavily insulated winter coverall was a part of the suit assembly. It appeared to be a satisfactory design, although somewhat bulkier and heavier than was considered necessary or desirable.

The flight tests in Alaska pointed out additional improvements that were needed, and as a result of this experience a revised design was made in February 1941. The garment consisted of an outer shell of all wool, cut on the bias for elasticity, with the wires sewn on the inside, as shown in Figure 1. Cotton-flannel cloth was used for the lining, because it is more comfortable against the body.

The clover-leaf shoe insert of the earlier model was supplanted by a wired-cloth shoe of olive-drab overcoating material with molded rubber soles which was worn inside of a standard light aviation boot.

Samples of this design were delivered to the test pilots at Patterson Field for test under actual flight conditions to determine if any further changes were necessary or desirable. The design was then standardized and the specifications written. This is the design which went into production in September 1941. Figure 2 shows a section of the factory in which these suits were made, and Figure 3 shows an operator stitching the wiring, using a special type of sewing machine.

Since the start of the original development, much time and effort have been spent to improve the design. No less than 100 variations have been tried of suit, glove, and shoe designs. Some of the earlier designs are shown in Figure 4. A much later design is shown in Figure 5. Ideas constantly change as new information is obtained in reports from combat areas, from Arctic regions, from tests constantly being conducted, and from new materials which are made available.

There are three general types of electrically heated clothing which have been considered and tried, and each has certain advantages and disadvantages. The first is the electrically heated undersuit of the type shown in Figure 1. The chief advantages of this design are:

1. The heating wires are close to the body, and all thermal insulation is outside, thereby giving good heating efficiency.
2. There is more simplicity in such a one-piece undersuit. There is less bulk around the waist than with a two-piece garment, and the electric connections are easier to make. There are no gaps for cold air to get in.
3. Clothing worn on the outside tends to become soiled from dirt, oil, and grease. The surface of an undersuit is protected by the coverall worn over it.
4. The design is considerably lower in cost, because the materials are less expensive and the tailoring is negligible. Less experienced help can be used to produce a satisfactory product.

The disadvantages are:

1. Having the wires so close to the skin tends to make the heat from the individual wires more noticeable.
2. The person wearing the suit, since it is an undergarment, tends to wear it all the time, sometimes even for sleeping. The wires which carry the current to do the heating will not withstand such continued rough usage over a long period of time.
3. A suit worn next to the skin requires more frequent washing. The use of light underclothing under the suit is recommended.
4. This clothing was designed to be worn under a light summer coverall. In case the flier is forced down in enemy territory and taken prisoner, he has no uniform or other suitable wearing apparel.
5. In case of a failure of the suit, or any part of it such as gloves or shoes, or a failure of the airplane electric system, or if the wearer is forced to bail out at high altitude or forced down in a location of low ground temperatures, he may have insufficient clothing to keep him warm.

The second general type of garment is an electrically heated uniform such as shown in Figure 5. This overcomes most of the afore-mentioned disadvantages, but again the wearer does not use it only as a flying garment.

The third general type is heated winter coverall which, like other winter clothing, is pulled on over the regular clothing and is definitely for flying only. It appears to overcome the objections previously listed except that of getting soiled. The heat distribution in this design is more uniform, and there is less tendency to produce local overheating, such as under the knees when a gunner is kneeling or on the inside of the elbow when the forearm is bent back at a sharp angle. The increased thermal insulation possible in such a garment makes it more comfortable for men who have to lean against the cold metal of the ship instead of a parachute pack. (Because of the pressure of the suit against the skin when the wearer is seated on the parachute pack, heating wires are omitted from the trouser seat.) With this better-insulated garment the heat distribution for the different requirements of the various crew members is met quite satisfactorily with the one design.

The problem of proper heating of hands and feet presents more difficulties than the suit itself. The shoes receive greater flexing than the suit, especially when walking, and consequently the heating wires are more vulnerable to breakage. There is also an individual variable in size and shape of foot. The shoes shown in Figure 1 are constructed of woolen overcoating material with rubber molded soles and were designed to be worn with rubber overshoes. Because the heat balance in the suit is not adjustable and has been arrived at by cold-room tests, it is quite necessary to maintain the proper balance in thermal insulation over the various parts of the body. For example, if *A*6 fleece-lined flying boots are used over the electrically heated shoes in place of the rubber overshoes for which they were designed, the feet may become uncomfortably warm in comparison to the rest of the body. If heavy fleece-lined clothing is worn in place of the summer coveralls along with the *A*6 boots, then the hands are too cold when the body and feet are comfortable, and a pair of mittens should be worn over the gloves.

Another type of shoe which has been developed and tested with very satisfactory results is an all-felt model shown in Figure 5. It has a thin leather sole to protect the thick felt insulating sole. These shoes have been worn at minus 60 degrees Fahrenheit without any additional covering and are very comfortable. The felt being porous permits any moisture from foot perspiration to pass out through the sides of the shoe, and the feet remain dry. One of the most important considerations where extreme cold is to be encountered is to make absolutely certain that the clothing remains dry. Otherwise the thermal insulating value is so greatly reduced that the feet especially become easily frozen. The use of heated clothing for Arctic flying is discussed more fully under the subject of tests.

At the present time the trend in thought in regard to electrically heated footwear is progressing in the direction of supplying a heated insert suitable for use with the *A*6 fleece-lined boot. These boots are standard issue to aircraft crews and have good thermal insulation. By the application of a lightweight heated boot insert, the flier is equipped for extreme cold with the minimum amount of additional equipment. Furthermore, the boot is waterproof and more satisfactory for walking through mud or wet snow such as might be encountered in certain ground locations. The felt-type shoes, however, are more satisfactory in actual flight or for use in cold rooms, because with the rugged sole and heel they are more comfortable for standing or walking.

Figure 3. An operator using one of the special sewing machines required for stitching the wire to the cloth

Figure 4. Early developmental samples of electrically heated clothing

Gloves inherently constitute the greatest problem in electrically heated wearing apparel. The greatest problem is to be able to keep the hands warm enough to do their job without becoming too cumbersome. In order to obtain the necessary finger dexterity the leather should be very thin and close fitting like a pair of women's dress gloves, but a hand covering of this type is almost colder than no covering at all, because it tends to restrict circulation, while loose-fitting well-insulated gloves are clumsy. Another difficulty arises, because the hand is frequently held in a closed position around some type of control wheel, stick, or machine-gun handle. Hence, wires on the inside of fingers and palm would not only be uncomfortably hot and humpy but also would subject the wires themselves to such severe flexing that their life would be extremely short. Consequently, electrically heated gloves at present carry the wiring only over the back of the hand and along the backs of the fingers with relatively thin thermal insulation throughout. This tends to produce an uncomfortable concentration of heat over the back of the hand and the knuckle joints when the fingers are bent, while the palm which is clamped over a cold handle and where the nerves which feel temperature are highly concentrated become uncomfortably cold. This problem is being studied constantly, and designs and samples too numerous to mention have been constructed. About the best solution to date seems to be to wear over the electrically heated gloves a pair of gauntlet-type mittens with alpaca, wool, or fur lining. These mittens are fastened to a cord or strap which is worn around the back of the neck, a system which has long been used in the arctic. These mittens furnish the necessary thermal insulation to keep the hands from getting cold and permit the heat being supplied to the back of the hand to distribute itself around the hand and fingers. When it is necessary to do some operation which requires use of the fingers, the glove hand may be easily withdrawn from the mittens temporarily and replaced before it gets cold. A similar method is to have the mittens heated and to wear unheated gloves inside. The former is preferable, since much of the time the temperature may not be so extremely low and the gloves are sufficiently warm without the mittens over them.

There is probably a question in the minds of some readers as to why an electrically heated head covering is not also provided. The face and head seem to be the least susceptible to cold of any part of the body, and many people go about on relatively cold winter days with no head covering at all. Fliers are provided with warm fleece-lined helmets, and from experience in cold rooms and in the arctic at temperatures of 50 and 60 below zero, it has been found that the head is comfortably warm with this unheated covering, provided that the remainder of the body is not unduly chilled. However, in cold as extreme as that just stated, in order to prevent frostbite, it is necessary to apply some type of covering to the exposed portions of the face. At sea level a fur ruff of the type worn by Eskimos, a woolen scarf, or some type of face mask will do. At altitudes above 12,000 feet, oxygen masks must be worn, which, due to the exhalation of the moist breath, choke up with ice which has to be broken away at intervals until finally the ice builds up too much at which time the mask must be exchanged quickly for another one. Heated coverings for these masks which keep the mask from freezing and also warm the face have been experimented with very successfully. At these temperatures even goggles frost over on the inside from the moisture of the face and become useless. Relatively low-wattage heaters have been developed which keep them clear under the most severe conditions. These items will become more necessary as fliers go to higher altitudes and colder temperatures.

The preceding sections have described the history and development of the electrically heated flying clothing worn by our fliers. It also has indicated briefly some of the problems which are encountered in any new development. Paralleling this work, investigations, studies, and tests have been carried on by laboratories throughout the United States and Canada where facilities are available in order better to understand the physiological aspects of the problem. There follows a brief résumé of some of those studies that apply to body heating.

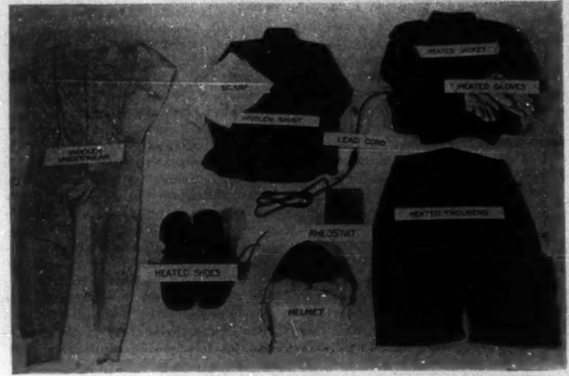


Figure 5A (above). Late developmental sample of two-piece heated suit

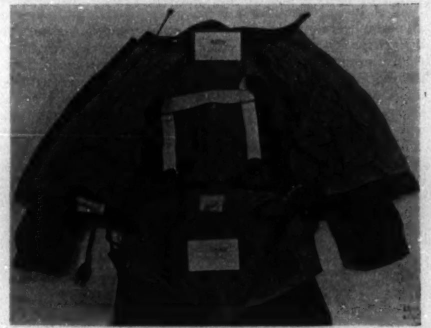


Figure 5B (right). View of wiring inside of suit

Figure 5C. (below) Man wearing the suit

Total Heat Requirements

The human body is a heat engine which oxidizes fuel to do work and maintain its temperature. When the body is at rest doing no external work, there is still internal work being done by the heart and other organs of the body and by muscles in maintaining their tension or tone. The body automatically balances the production and loss of heat to maintain an almost constant temperature of 98.6 degrees Fahrenheit under quite widely varying external temperature conditions. Temperature control is accomplished by the heat-regulatory center in the brain which is extremely sensitive to temperature variations and is completely involuntary. Throughout a considerable temperature range the body is able to control its temperature by varying the rate of heat generation and the rate of heat loss. If the temperature, however, goes outside of these limits, secondary considerations manifest themselves such as shivering which is a rapid flexing of the muscles to increase their rate of heat generation.

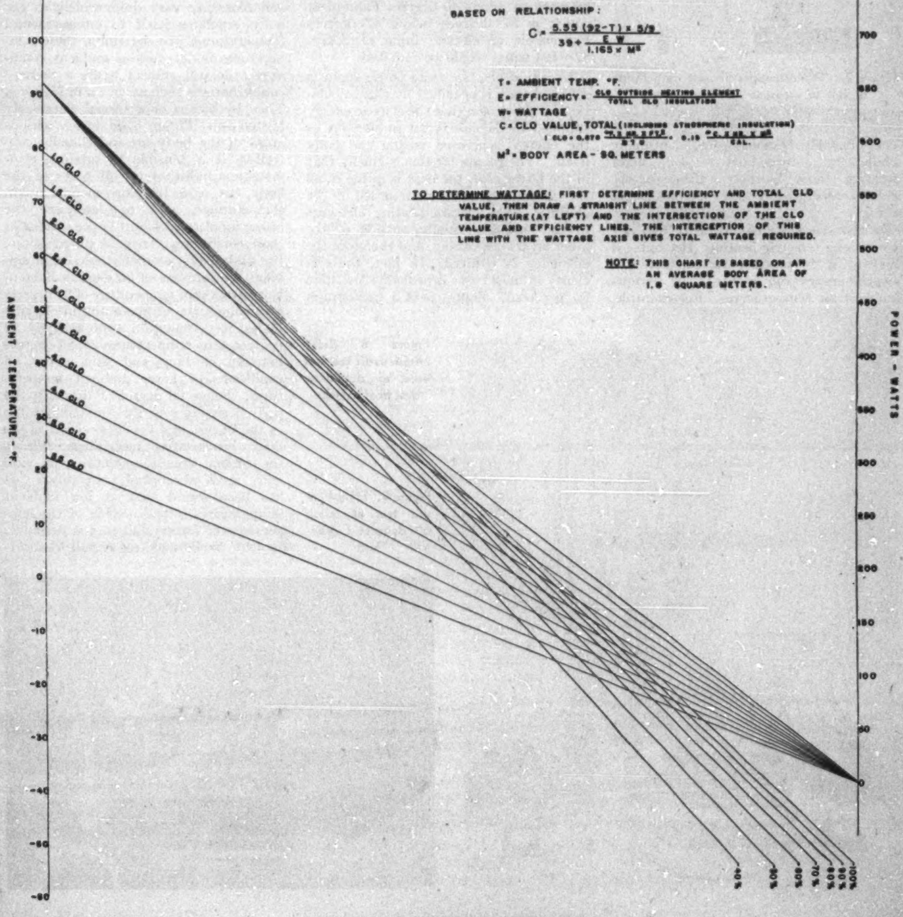
The amount of heat generated by the human body varies widely with the amount of activity. The base rate of heat generation called the basal metabolism rate is that of a body sitting down completely at rest but not asleep and in a comfortably warm location. The slightest amount of exertion even sitting up or reading or eating increases this basal rate, and, for a person exercising violently, the rate may go up 12-fold. In the case of aircraft pilots or other crew members in normal conditions of flight an average value of 100 calories or 400 Btu per hour is assumed.

Of course, not all of the heat generated by the body is transferred to the air through the clothing. A certain amount is radiated from the exposed portions of the skin such as hands and face, a certain amount is lost by perspiration, and other amounts are lost by evaporation to and temperature rise of the air which is breathed. The amount of heat lost by each process varies with conditions of relative humidity and temperature. For example, when the air temperature is above body temperature, all of the heat loss must be by evaporation. DuBois[4] shows that in still air at temperatures below those producing sensible perspiration, the heat loss is made up, approximately, 25 per cent by vaporization of water, 15 per cent by convection and 60 per cent by radiation. These percentages of course will vary also with the amount of clothing and the wind velocity.

Determination of Heating Requirements as a Function of Clo Value

During the past few years and especially within the past year and a half, many laboratories throughout the country have been experimenting on the types of clothing best suited to the various atmospheric conditions which exist throughout the world and in the cold rarified air above it. It has been found that the nude body at rest is comfortable in an ambient temperature of 86 degrees Fahrenheit and that when it is covered by normal indoor clothing in an ambient temperature of 73 degrees Fahrenheit the skin temperatures and body comfort are approximately the same. The insulating value of this amount of clothing is approximately equal to the "clo" unit as defined by Gagge, Burton, and Bazett[5] and will be so used in this paper for simplicity. For additional clo value the temperature may be dropped another 13 degrees. For example, with clothing having a two-clo value the body is comfortable at 60 degrees Fahrenheit, with three-clo value at 47 degrees Fahrenheit, and for four-clo value at 34 degrees Fahrenheit. Since the best Arctic clothing does not exceed 4 or 4 1/2 clo units, it may be seen that it is impossible to keep an inactive man comfortably warm indefinitely at temperatures much below freezing. The slightest activity, however, increases the metabolic rate considerably, and, as a result, a man wearing the same four clo and walking at a rate which raises his metabolism threefold would be equally comfortable at minus 70 degrees Fahrenheit providing his face was protected from frostbite. DuBois[4] illustrates this by one of his tests. A subject wearing a woolen ski suit and woolen socks lay completely relaxed in a calorimeter with the ambient temperature automatically controlled at 74 degrees Fahrenheit. Within two hours the subject complained of being uncomfortably cold, although a few days before he had worn the same clothing while skiing in an ambient temperature of minus 16 degrees Fahrenheit without discomfort. The author found that in walking at minus 40 degrees Fahrenheit the difficulty was to prevent becoming overheated and perspiring which causes considerable dis-

Figure 6. Nomograph chart for determining wattage required



BASED ON RELATIONSHIP:

$$C = \frac{5.55\,(92-T) \times 5/9}{39 + \frac{E\,W}{1.165 \times M^2}}$$

T = AMBIENT TEMP. °F.

E = EFFICIENCY = $\frac{\text{CLO OUTSIDE HEATING ELEMENT}}{\text{TOTAL CLO INSULATION}}$

W = WATTAGE

C = CLO VALUE, TOTAL (INCLUDING ATMOSPHERIC INSULATION)

$1 \text{ CLO} = 0.878 \frac{°F. \times HR. \times FT^2}{BTU} = 0.18 \frac{°C. \times HR. \times M^2}{CAL.}$

M² = BODY AREA - SQ. METERS

TO DETERMINE WATTAGE: FIRST DETERMINE EFFICIENCY AND TOTAL CLO VALUE, THEN DRAW A STRAIGHT LINE BETWEEN THE AMBIENT TEMPERATURE (AT LEFT) AND THE INTERSECTION OF THE CLO VALUE AND EFFICIENCY LINES. THE INTERCEPTION OF THIS LINE WITH THE WATTAGE AXIS GIVES TOTAL WATTAGE REQUIRED.

NOTE: THIS CHART IS BASED ON AN AN AVERAGE BODY AREA OF 1.8 SQUARE METERS.

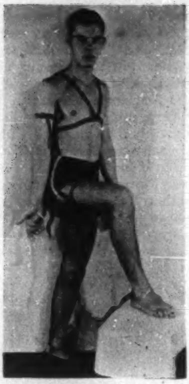


Figure 7. Thermocouple harness worn inside of suit to measure skin temperature

comfort. He became quite thoroughly chilled in a three-hour airplane flight wearing more clothing, although the cabin temperature was ten degrees above zero.

When the total clo value of clothing containing electric-heating elements is known, it is possible to calculate the electric-energy requirements for various ambient air temperatures. For example, if the clothing has an insulating value of three clo without the application of any electric heat, the wearer would be comfortable at a temperature of 47 degrees Fahrenheit (86 minus 3×13 degrees). It is assumed that of the total metabolism of 100 calories per hour (400 Btu) 75 per cent is lost through the clothing by convection and radiation. Therefore, doubling the metabolic rate or putting in an equivalent electric input, (75 calories equals 87 watts) would keep the wearer comfortable down to a temperature of eight degrees Fahrenheit (47 degrees minus 3×13 degrees), assuming that the electric energy is 100 per cent efficient in producing body warmth. This is an input of 2.23 watts per degree below 47 degrees Fahrenheit. If the suit is to be worn at minus 40 degrees Fahrenheit which is 87 degrees below 47 degrees Fahrenheit an electric input of 2.23×87=194 watts would be required.

In actual practice more power input is required than is obtained by this calculation for the reason that the electric energy supplied to the suit is not so effective as the energy generated within the body itself. The reason for this is chiefly that in the latter case, the heat is inside of all the thermal resistance represented by the clothing. The electric-heating elements in the suit are usually outside of the inner layers of clothing, and therefore the efficiency is reduced. In fact, the efficiency in most cases is probably less than 75 per cent. Figure 6 is a nomograph chart showing how to determine the wattage requirements for electrically heated clothing when the clo value and the efficiency are known. The method of determining the efficiency also is shown. While many of the physical factors of the body cannot be determined as precisely as with electric or mechanical machines because the body helps to compensate for variables over a fairly wide range, nevertheless, the chart indicates quite accurately the result of experience to date.

Normal Body Temperatures and Basic Heat Distribution

It is a well-known fact that except in sickness body temperature when taken by rectal thermometer is very constant, but the temperature of arms, legs, hands, and feet may vary quite widely as the body regulates itself to environmental temperatures. To determine these temperatures under various ambient conditions, the test subject wears a thermocouple harness such as shown in Figure 7. Then by means of a special electrically heated suit, Figure 8, in which various areas of the body are individually controlled, it is possible to produce comfortable conditions on all parts of the body, to read the corresponding skin temperatures, and to determine the power input to each part of the suit under these conditions. Since, as stated before, the nude body is comfortable in an ambient temperature of 86 degrees Fahrenheit with a skin temperature of 92 degrees Fahrenheit, the ultimate aim in clothing design is to maintain approximately 92 degrees skin temperatures or 86 degrees between clothing and skin under all conditions of lower ambient temperatures. Below 86 degrees Fahrenheit the body begins to cool, and the temperature of the fingers and toes goes down as the body heat-control mechanism reduces the cooling area to maintain the vital body areas at normal temperature. It has been found that at low ambient temperatures some lowering of the temperature of fingers and toes is desirable. In fact, hands and feet remain comfort-

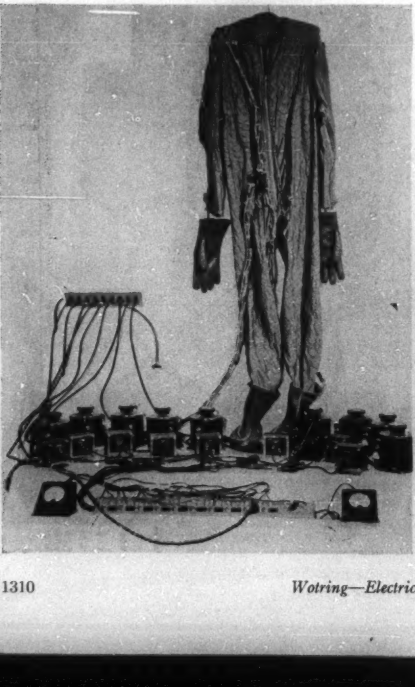


Figure 8 (left). Multicircuit test suit used to determine best heat balance

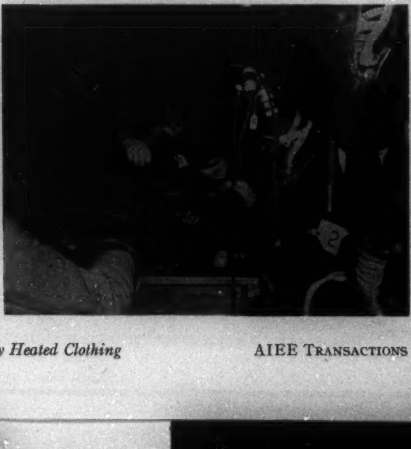


Figure 9. Eight-hour suit test at minus 60 degrees Fahrenheit

able at temperatures down to 60 degrees and do not feel cold to the individual until they are below this temperature. Temperatures down to 40 degrees Fahrenheit are not considered dangerous although they are decidedly uncomfortable. When proportioning the heat input to the various parts of the body, it is desirable not to heat the hands and feet too warm, because such heating tends to throw the normal body regulatory system out of adjustment and the individual may lose heat too rapidly with a resultant dangerous lowering of the temperature of the vital organs.

Over the vital areas of the body, chest and abdomen, skin temperatures below 90 degrees Fahrenheit feel increasingly uncomfortable, and 80 degrees Fahrenheit is considered to be the lower limit of endurance.

Because of the body's ability to regulate its own temperature throughout fairly wide limits of ambient temperature, there is no absolutely correct distribution of electric heat. Different investigators arrive at somewhat different results, but probably many are sufficiently close. The distribution also is influenced considerably by the thermal insulation available. For example, our fliers are issued fleece-lined boots known as the *A*-6. An electrically heated shoe worn inside these boots will require a different electric input for foot comfort than if a different type boot is worn. Also, because of the fact that the boots can be much better insulated than the gloves if finger dexterity is to be achieved, the wattage requirements are lower, even though the feet make up a larger percentage of body volume than the hands.

The following gives a distribution of heat which has proved to be quite satisfactory. It should be kept in mind, however, that these values can be greatly modified by distribution of thermal insulation.

Jacket	45 per cent
Trousers	25 to 28 per cent
Gloves	7 to 8 per cent
Shoes	5 to 6 per cent

As has been pointed out previously, the distribution of the wires in each part of the suit also is influenced by the wearer's occupation. A pilot sitting for long periods of time on a thick silk parachute pack will become uncomfortably warm if there are wires in the lower back of the jacket or in the seat of the trousers. Without heat in this region a waist gunner or tail gunner may feel uncomfortably cold. This is especially true of the ball-turret gunner who wears no parachute. Tail gunners frequently operate their guns while kneeling, and unless allowances are made in the wiring diagram the suit would become uncomfortably hot where the leg is doubled back on itself. To avoid having a multiplicity of suits for different uses, one suit having the best average distribution is desirable. This can be done by:

1. Keeping the total watts low which means good thermal insulation.
2. Keeping the temperature of the individual wires relatively low by spacing them closely (this has to be balanced by the fact that an excessive wire length is a waste of material and makes the suit heavy).
3. Distributing the heating effect of each individual wire by using sufficient insulation between the skin and the wire surface.

By properly weighing these various factors a suitable garment can be designed.

One of the suggestions made many times is to the effect that since it is the extremities, fingers, toes, and then hands and feet which first become cold, why would it not be sufficient to heat only these extremities? A little study proves why such heating is unsatisfactory where cold temperatures are encountered. By studying available medical literature, it was found that a large man's foot has a volume of about a quart and a hand of approximately a pint, which makes the total of roughly three quarts for the two hands and feet. The minimum blood circulation through hands and feet is about one per cent of the volume of the tissue per hour or four pounds of blood per hour. If, as has been found, an electric input of 200 watts (680 Btu) is required to keep a man wearing an electrically heated suit comfortable at minus 60 degrees Fahrenheit, it can be seen that the temperature of the blood in the hands and feet would have to be raised 170 degrees Fahrenheit above body temperature to transfer the heat required. Skin temperatures in excess of ten degrees Fahrenheit above body temperature are extremely uncomfortable, and so it follows that such a system is not satisfactory where any appreciable heating is required.

Arctic Use

The question is frequently asked, "What happens if a man is forced to land or bail out while flying over regions of low ground temperatures? Would he not freeze to death?" The answer to this question is that he would do just what he now does with unheated arctic-type clothing. It has been pointed out that with a well-insulated arctic suit a man can sit comfortably without muscular activity only at temperatures above 25 to 30 degrees Fahrenheit for more than two hours. However, with the same clothing he can keep comfortably warm while walking in temperatures down to 30 degrees or more below zero. The answer, therefore, is to wear a suit of good thermal insulation over the heated garment or a well-insulated type of winter coverall with the heating element built into it. Then the protection will be as good as at present. Anyone who flies over arctic regions should know how to exist on the ground until help arrives. Such information is issued in pamphlet form to all of the members of our air forces in those regions. Among the items carried in the airplane is always a good

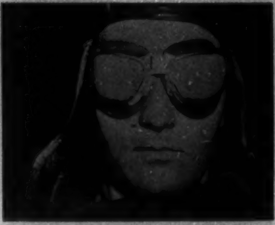


Figure 10A. Effect of cold on unheated goggles

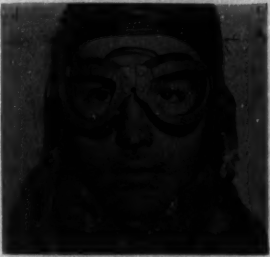


Figure 10B. Same with adapter-type heaters

Figure 11. Mechanical-man suit tester

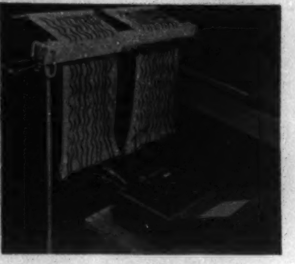


Figure 12. Wire-flexing test machine

sleeping bag in which a man can rest quite comfortably for several hours with ground temperatures of 40 or more degrees below zero. The airplane also carries emergency rations and other emergency equipment, and the airplane itself serves as a visible landmark to searching fliers. As a consequence, fliers are advised that if it is at all possible it is better to crash land the airplane than to bail out of it.

Cold-Room Tests and Effect of Altitude

Up to the present time few fliers have ever flown in temperatures as cold as 60 degrees below zero Fahrenheit. However many of the tests in cold rooms are conducted at that temperature to determine the suitability of the various articles of clothing which include face masks and goggles. By testing at such extreme temperatures, we know that the flier will be comfortable under all of the less severe temperature conditions which he will encounter. Furthermore, temperatures as cold as 60 below zero are seldom found except at high altitudes, 30,000 feet or more above the surface of the earth. At those altitudes the density of the air is much lower, and its capacity to carry away heat by convection is also less. To test this effect with a heated suit, the same subject wearing the same clothing was tested at minus 60 degrees Fahrenheit both at sea level and at an equivalent altitude of 35,000 feet. The wattage to the heated clothing in the second case was reduced until the same skin temperatures were measured, and the individual was satisfied that he was equally comfortable from a temperature standpoint. The wattage input was found to be approximately 25 per cent less than at sea level. Of course, the amount of this reduction depends upon the total clo value of the suit, the wind velocity, and so forth. Additional testing on a number of subjects will be necessary before the quantitative effect of altitude can be stated authoritatively.

One of the factors which no doubt has some significance when comparing cold-room tests and actual flight tests is purely psychological. A man sitting in a cold room for a period of four or more hours with nothing to do but to try to decide whether he is warm or cold is more likely to be critical of very minor effects than a flier who is over enemy territory on a mission. The latter is too occupied with other matters to be much concerned with his physical comfort. This has been pointed out by cases of men who have flown on bombing missions without electrically heated clothing under temperature conditions which would be almost unendurable in a testing laboratory. However, it is felt that if the suit is satisfactory from a heat balance and total heat standpoint in the laboratory, it will be at least as good under actual flight conditions.

One very interesting cold-room test which has received considerable publicity was run in the General Electric Company cold room in Bridgeport. A group of nine men, three from Wright Field, three from the Harvard University fatigue laboratory, and three from General Electric Company went into the room in the morning, and all but one spent eight hours in the cold which was at all times between 55 and 70 degrees below zero Fahrenheit. One individual who was wearing standard unheated arctic clothing consisting of woolen underwear, woolen shirt and trousers, and a shearling winter flying suit and boots, was forced to leave the chamber after two hours because of very cold feet and was beginning to become chilled all over. After this experience he became exceptionally tired, probably due in part to the energy his body had used up trying to maintain its normal temperature. All of the other eight individuals wearing electrically heated clothing with various combinations of other arctic clothing remained for the full test, and none felt any worse for the experience after it was over. During the eight-hour test, these men read magazines, carried on conversation, played cards (Figure 9), and ate a good lunch from an especially built electrically heated food cabinet within the room itself. The test proved conclusively that properly designed electrically heated clothing will keep an individual comfortably warm on the longest and coldest of flight missions.

Heated Face Covering

It has been previously mentioned that under conditions of extreme cold it is necessary to protect the face. The arctic explorers who had only body heat available wore a deep wolf- or wolverine-fur ruff which could be drawn in closely about the face with only a small space to see through and breathe through; see Figure 9. Such a face covering is not satisfactory for an aircraft crew, since wide angle of vision is necessary, and the eyes should be protected against possible flash burns as well as the cold. Hence, some type of goggles is very desirable in combat areas. In cold locations, however, the goggles almost immediately condense the moisture given off from the surface of the eyes and surrounding skin, and within a few minutes they are so completely fogged and frosted on the inside as to be useless; see Figure 10. Heaters have been designed and built for several types of goggles which not only keep them clear of moisture indefinitely but also warm the air in front of the eyes.

At high altitudes, or in fact at any altitude above 12,000 feet, it is necessary to wear oxygen masks. By making absolutely sure that the supply of oxygen in the tank is dry, no particular difficulty is encountered in the supply system. Exhalations, however, are valved out through the mask, and the moisture causes the mask and valve ports to fill with ice. Breathing the extremely cold oxygen, like breathing air on a cold day, causes considerable watery discharge from the nose. This plus the exhaled moisture builds up ice inside of the mask and in the exhaust ports, making breathing first uncomfortable, then difficult, and eventually impossible. Fliers frequently carry one or more extra masks for this reason. A heated cover over the mask and the supply tube not only warms the oxygen breathed in and prevents the mask from icing up, but the warmed mask is much more comfortable against the face and prevents frostbite.

The application of electric heat, therefore, to the face covering is not only a desirable adjunct to electrically heated clothing, but will become a necessity when airplanes fly at the high altitudes now contemplated.

Types of Controls

The wattage input necessary to maintain comfort in electrically heated clothing at sea level varies directly with the decreasing ambient temperature from the value of zero input at approximately 45 degrees Fahrenheit to 100 per cent at the design temperature value of minus 60 degrees Fahrenheit. Since the wearer will experience all of these temperatures while going from a region of moderate ground temperatures to the altitude where the minimum temperature is reached, it is necessary to be able to vary the electric input. Military airplanes in which it is expected that electrically heated clothing will be worn are provided with rheostats at all crew positions. Each man therefore has the means of adjusting the power input to suit his particular activity and location. This is a satisfactory arrangement but does require some conscious effort on the part of the wearer to prevent his becoming overheated.

Present experience indicates that a satisfactory type of control is one similar to that used on the automatic electric blanket in which a thermostat with a heating element is connected in parallel with the load and exposed to the ambient-temperature conditions. The thermostat will then "cycle," the per cent time on being in direct proportion to the decrease in ambient temperature, until at the lowest design temperature it remains on full time. A controllable resistor in series with the thermostat heating element permits the wearer to adjust the per cent time on within a narrow range to compensate for individual differences. For the suit control a small sensitive thermostat with its heating element and the necessary aircraft-type current-interrupting relay are built into a compact unit which may be located in the suit connecting cord. With this arrangement the wearer of the suit plugs the suit cord into the receptacle in the rheostat located in the airplane and turns it to the "full on" position. The automatic control thereafter will take care of the heating requirements in proportion to the ambient-temperature condition. Should any failure occur in the automatic-controlling element, it can be short-circuited out with a switch provided for the purpose, and the suit may still be controlled by the manual rheostat. In addition to its automatic feature, the control eliminates the heat which otherwise is lost in the resistance of the rheostat. With the ever increasing amount of electric equipment being used on airplanes and the necessarily limited capacity of the generators, any such power reduction is a decided advantage.

One variation which might be desirable is to have a small thermostat in each glove and shoe which would help to compensate for possible differences in the amount of hand or foot covering. The relatively wide temperature range allowable for hand and foot comfort and the relatively low watts required make the small thermostat practical for this application.

Life Tests

Since a failure of the electric circuit of a suit on a cold flight can be not only uncomfortable but also serious, the suit must last as long as possible. The life of these suits depends almost entirely upon the electric wiring and not upon the strength and life of the suit material which is the case with unheated clothing. Since the first suit was constructed, a variety of tests has been made to locate and correct by redesign the points where failures occur. As a result, the operating life has been and is still being constantly increased. One of the tests devised for the purpose of determining the points of failure is the mechanical man shown in Figure 11. His hands and feet are connected to crank arms which cause his arms and legs to move through many of the equivalent motions of a flier in operating an airplane. Accurate resistance measurements of the suit are kept daily, and, when the resistance begins to change, it indicates a breaking down of the electric-resistance wires which produce the heat. The suit then is carefully examined by making additional resistance measurements and by noting any deflection of the resistance-bridge galvanometer as different parts of the suits are flexed. In this way the point of failure can be located. A study of the conditions producing the failure is made, and, by shifting the position of the wires, or changing their direction to run parallel to the line of bending, it is possible to reduce the mechanical forces which caused the failure. As soon as the correction is made, the revised suit is put on the mechanical man. A counter on the mechanism records the total number of revolutions of the main crank and permits comparisons among different tests.

Figure 13. Dairy-route delivery man wears suit for mechanical life test

The wire itself constantly is being tested to find new and improved materials and methods of stranding. Figure 12 shows a testing machine which bends the wire through 180 degrees at a rate of 40 cycles per minute, and again a change in resistance measurements gives the first indication of failure.

A shoe tester and a glove tester have been used but have not been so successful as the clothing tester. In order to determine the life of the shoes, actual walking tests were made. Various members of the engineering office and developing and testing staff walked in shifts, two miles a day, over a period of several months. At the end of each two-mile walk the shoe resistances were checked for possible indications of failure. In this way shoe life was raised from ten miles to well over 200 miles, and improvements now in progress would indicate that this mileage can be doubled or trebled.

Another method of testing the clothing was to have them worn by people whose occupation required considerable activity: walking, bending, and using the hands. Nine route delivery men for a dairy concern (Figure 13) volunteered to test suits of a new design. They were issued suits, and one of the laboratory staff visited the dairy once a week and made accurate resistance measurements. This test made it possible to determine the comparative life of suits either in hours of active wear or in miles of walking. These tests also were very helpful in producing a better product.

The Future of Electrically Heated Clothing

After the war is over it is not anticipated that there will be any wide need for complete electrically heated clothing such as is being developed for our combat aircraft crews. There still will be some demand for it by men who work in cold laboratories developing aircraft equipment.

For high-altitude flying of commercial passenger airplanes after the war, cabins no doubt will continue to be pressurized and heated. However, freight airplanes flying the polar routes probably will not be pressurized, and electrically heated clothing may be worn by the crew even if only as an insurance against possible failures of the regular heating system. Mechanics and ground crews stationed at those cold locations and even throughout northern United States and Canada may use this clothing. For mechanics on the ground less insulation could be used with an increase in electric input, since power is generally available at repair stations. The reduced bulk of the insulation would permit easier body movement. Sentries, guards, and lookouts at cold locations, including those on ships who do not move far from a fixed location may use heated clothing. Traffic policemen who stand on cold windy corners could be made quite comfortable by wearing heated clothing which could be plugged in when they are at a fixed location.

Very little thought, however, is being given to such postwar uses. The effort of those engaged in this highly interesting development is still being used to produce the best possible heated clothing for our Air Forces.

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A-C Supplies for Services in Large Aircraft—a British View

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Synopsis: The paper deals with the increasing use of electricity as a source of power for operation of services in large transport aircraft. It outlines the advantages which are obtained from the use of medium-frequency a-c electricity and stresses the saving in weight which is obtainable from raising the present voltage (30 volts direct current) to around 200. Some suggestions are made in regard to form and layout for postwar installations in the case of both land airplanes and flying boats of the larger sizes.

THE use of electricity for operation of aircraft services has increased very greatly in the past ten years. Prior to 1934 there was little electric equipment installed in British aircraft, the main use for it at that time being in connection with navigation and cockpit lighting and radio. Transport aircraft seldom carried d-c generators of more than 300–400 watts capacity, and these were of the windmill-driven type, third-brush or voltage-controlled and, following automobile practice, operating at 12 volts.

About this time and following its successful development in the United States, electric starting of engines was adopted, first for military aircraft and later, when its reliability and advantages had been established, for the larger types of transport aircraft also. By 1936–37 many of the British medium-sized transport aircraft carried electric starting equipment and the then new 20-ton Short flying boats of the Empire Service of Imperial Airways were of course so fitted. It is our view that the successful employment of electric machines for engine starting constituted a powerful stimulant to the development of electric apparatus in general for aircraft purposes, and designers and operators alike were quick to realize its advantages once the bogy of reliability had been laid. At least one all-electric aircraft was produced, but it was doubtful whether the equipment had reached a sufficient stage of development to establish the principle at that time.

The initial period of expansion of the Royal Air Force, preceding the war years, while tending to slow up development of transport aircraft in England, saw a considerable increase in the number of electrically operated services in operational aircraft, and at least one all-electric bomber, the Short Stirling, was produced. So far as is known, this machine has been satisfactory and reliable in operation.

All these developments in electricity have played their part in establishing in the minds of those responsible for aircraft design that all-electric operation of services is more than a possibility, and today, when once more the design of large transport aircraft is in mind, there is undoubtedly a solidification of thought in the direction of electricity.

The electrical designer therefore is confronted by a wide field for his ingenuity. Generating equipment, although uprated and operating in numbers, is really inadequate for its purpose; cable runs have increased in length out of all knowledge; the necessary range of individual motor power is greatly expanded; various types of electric actuators are demanded; and these and many other factors make it necessary to evolve complete electric systems, always with the overriding considerations of reliability and economy in weight.

To return for a moment to the past, in the years 1935–37 when developments on these lines had been foreseen, a good deal of experimental work had been done in the United States on single-phase a-c systems at a frequency of 800 cycles and also three-phase at 400 cycles. The authors and others working on the same problem had come to the conclusion that for aircraft of approximately 100,000 pounds weight and over a three-phase 400-cycle system operating at about 100 volts would provide the best basic installation. This view was put forward by a colleague, R. H. Woodall, in the discussion following a paper delivered before the Royal Aeronautical Society in 1939 by Chaplin and Nixon. Work had been started on this project in 1938 but was stopped in 1939, and it was not until 1941 that resumption was possible, this time at a modified frequency of 250 cycles for a large projected flying boat.

Since this time improvements in insulation, greater experience, and familiarity with the subject lead the authors to the view that it is possible where required to increase the line voltage to 200 or over, and that the optimum frequency remains at 400 cycles. On this basis it is possible to provide a light and reliable power system which will show advantages in most respects over alternative schemes.

The authors have endeavored in the following pages to provide justification for their views and to describe the lines upon which development is proceeding in connection with electric equipment for a large machine whose auxiliary-power requirement will be of the order of 100 kw.

Advantages of Three-Phase A-C Systems Over D-C Systems

The choice of a three-phase 400-cycle system is based on the following points which the authors believe accord closely with the views held in the United States:

1. The simple robust construction of a-c generators and squirrel-cage motors.
2. Absence of commutators which reduces size and weight and enables full use to be made of higher operating speeds.
3. Elimination of filter circuits at present associated with d-c circuits and avoidance of excessive brush-wear phenomena, associated with high-altitude operation.
4. Replacement of rotary transformers by lighter static a-c equipment and dry-plate rectifiers.

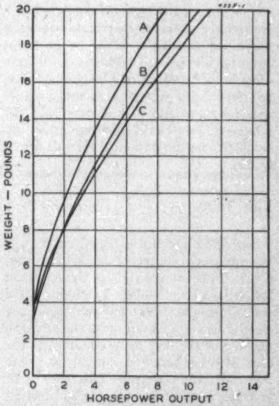

Figure 1. Weights of motor–gearbox units to give a final shaft speed of 2,000 rpm (continuously rated 400-cycle induction motors)

The difference in over-all weight between the four- and two-pole units is small

A—Six-pole 8,000-rpm unit with 4:1 gearbox

B—Four-pole 12,000-rpm unit with 6:1 gearbox

C—Two-pole 24,000-rpm unit with 12:1 gearbox

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5. The simple and light design of switchgear and contactors made possible by the ease with which a-c arcs are extinguished, particularly on the higher-frequency system proposed.

6. Reduction in maintenance resulting from the absence of rotating machinery and rubbing contacts.

7. The ability to use direct discharge lighting.

8. The ability to use resonance and phase-displacement effects in the design of remote-indicating instruments and controls.

9. The ability to operate a-c machines satisfactorily at higher voltages than d-c machines, and the saving in weight effected thereby.

The aforementioned factors are self-explanatory, except perhaps in the case of the saving in weight mentioned in the last paragraph.

It will be readily agreed that little or no weight saving may be effected by altering the operating voltage of either d-c or a-c machines, but a considerable saving is attained on cable weight if the system is operated on a higher voltage. For instance, to transmit 100 kw over a distance of 100 feet on a 30-volt two-wire d-c system requires a cable weight of 4,600 pounds or two tons. To transmit the same power on a 200-volt three-phase three-wire system at about 0.8 power factor requires 100 pounds. There is thus a very great advantage to be gained by operating the system at a higher voltage, and, while the use of high-voltage direct current would give about the same reduction in cable weight as with alternating current of the same voltage, switching and commutation troubles at high altitudes among other factors leave the advantage with the a-c system.

Table I. Some Services for Which Electricity Is Needed, or May Be Used

Motors for:
  Main-engine starting
  Undercarriage legs and doors
  Wing flaps
  Ailerons
  Elevators
  Rudder
  Trim tabs
  Pressure cabins
  Petrol cocks
  "Automatic pilot" and gyroscope
  Propeller control (feathering, and so forth)
  Deicing gear
  Cowl gills and radiator shutters
  Fans and air pumps
  Fuel and vacuum pumps
  Windscreen wipers
  Aerial winch and aerial control
  Landing lamp motors
  Jettison doors
  Luggage or cargo winch

Indicating Instruments
  Oil pressure, flap position, and so forth

Illumination
  All forms of lighting (crew and passenger compartments) including illuminated signs, such as "Keep Your Seat," and all signal or identification lamps

Heating
  For all kitchen and buffet requirements, radiators and emergency flying suits (for flight mechanics)

Radio and Interphone Requirements

Sound-Signal Devices
  "Take-off" horn, and so forth

Miscellaneous
  Cigar lighters, passenger call bells, and so forth

It has been stated by some critics that a serious disadvantage of the a-c system lies in its inability to cater for storing electric energy for ground and starting purposes. In the opinion of the authors, this constitutes no problem in the cases considered, since these large aircraft undoubtedly will operate from selected sites equipped with ample ground facilities, and, should it prove desirable to have a stand-by electric source for emergency ground operation, the view is taken that this will be best achieved by the use of a small auxiliary generating plant, the size of which will be influenced by the power required for engine starting.

The batteries necessary for starting some of the newer projected engines would far outweigh an equivalent auxiliary generating plant, particularly when it is considered that no provision for altitude operation of the small engine need be made.

Main Features of a Three-Phase System

Alternative Forms

A-c supplies normally are generated at constant frequency and constant voltage, but, if use is to be made of the aircraft engines, then such supplies will not be obtained over the full range of engine speed unless some special form of drive is used. If the alternators are coupled directly or through a fixed-ratio gearbox, then frequency and voltage and the speed of all a-c motors will vary with engine speed. Hence, with main-engine-driven alternators, there are at least two possible forms of supply, namely:

1. Constant frequency and constant voltage.

2. Varying frequency and varying voltage (in which motors and generators operate at constant flux).

For purposes of comparison, it is assumed that electric power is to be generated and utilized at all engine speeds between idling and take-off, and that the same frequency and voltage are to be generated by both systems at defined cruising speeds. Motors which are to operate a given service only at cruising speeds therefore will be of the same size and weight in each case. With these provisos the authors consider the constant-frequency and constant-voltage system to be the more economical. The chief advantages over the varying-frequency and varying-voltage system may be summarized thus:

A. The total bulk and weight of electromagnetic apparatus is reduced, since no provision is necessary for increased losses at high frequencies, or for the lower output of motors and generators at the lower speeds and frequencies. Definite figures cannot be quoted, since much will depend upon the frequency range and the periods during which the frequency differs from the cruising value.

B. No special generating set or regulating device is needed to give the constant-frequency and constant-voltage supplies needed or desirable for services such as radar, gyroscopes, heating, cooking, and lighting.

C. Although a variable-frequency system may be suitable for large military machines where full engine speed, and therefore full electric motor power, is available during combat periods, the conditions under which civil airplanes operate are quite different. In the first place, engine speeds are decreased steadily during a long flight in order to economize in fuel, while, in addition, the proportion of constant-frequency constant-voltage equipment is larger than in a military machine.

D. The idea of a variable-frequency system finds no favor with air-line operators; who always have been accustomed to the constant frequencies of commercial supplies.

The authors therefore argue that a varying-frequency varying-voltage system is not suitable for a transport machine.

Alternator Drives

Although the authors have expressed a preference for a constant-voltage and constant-frequency system and have considered that this is best provided by alternators driven from the main motors, it will be as well at this stage to examine methods by which the alternators may be driven and to give further reasons for the ultimate choice.

Alternators on aircraft may be driven by one of the following four methods or, if required, by a combination of them:

1. By the main engines through a single-ratio or multiratio gearbox.

2. By the main engines through a constant-speed drive.

3. By auxiliary engines.

4. By gas or steam turbines.

To consider these methods in further detail:

1. The driving of alternators from the main engines through a fixed-ratio gearbox, while having the important virtue of simplicity, produces a supply of varying voltage and frequency, the disadvantages of which already have been stated. Such a system, we understand, may be adopted as a temporary expedient on certain military aircraft for want of a better, but it is only likely to be usable because in such a case the power required at constant voltage and frequency amounts only to a small proportion of the total generated output.

A rather better scheme, though somewhat more complicated, makes use of a multiratio gearbox with automatic gear change. With this arrangement a speed variation of within ±15 per cent is said to be obtainable over a range of engine speeds from 950 to 2,700 rpm (3:1 approximately), which of course reduces the frequency range over which the motors must operate and eases the duty on the voltage-regulating system. It is understood that a mechanism on these lines is being developed, using centrifugally controlled oil pressure and that its weight will be approximately 90 pounds for an output of 50 kw with a five-minute rated overload of 50 per cent. The authors do not see such a scheme as an ultimate solution to the problem, however, on the grounds of speed variation and weight.

2. For many years attempts have been made to develop a simple and reliable constant-speed coupling, so far without very much success.

In the United States it is believed that there is an interesting project for a hydromechanical form of drive in which the relative speeds of annulus and sun gear of an epicyclic train are controlled by hydraulic methods, a centrifugal governor being provided to control the hydraulic flow for constant speed of output shaft with varying input speed.

In Great Britain work is proceeding on similar lines and also on an adaptation of the Hayes gearbox which at one time was fitted to certain of our automobiles. Little is known of the latter scheme, but it is anticipated that the former will provide a light, efficient, and reliable unit which should be capable of maintaining substantially constant alternator speed over a 3:1 drive range. It is possible that, through a refinement of this, paralleling of alternators may be achieved, although as at present contemplated the electric system does not demand it.

3. The provision of the entire electric supply from an auxiliary generating plant in the past has been considered as an attractive solution of the a-c generating problem, and much work has been carried out on suitable small power plants, both in Great Britain and the United States.

Such a system is being used on a new medium-altitude flying-boat project in Great Britain which is to be all a-c-equipped and is to derive its power from two separate auxiliary-generating-plant units. The authors have not at their disposal complete figures for the installation which has the advantage of simplicity and of being self-contained. The engines, of course, must be of greater size and, consequently, weight than the electric output demands, because of the necessity for operation at altitude, although this in the project concerned is less serious than would be the case in a machine which was to fly at levels now envisaged of the order of 25,000 feet or over.

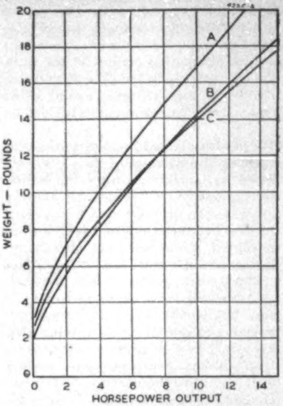

Figure 2. Weights of motor–gearbox units to give a final shaft speed of 2,000 rpm (90 seconds rated 400-cycle induction motors)

The difference in over-all weight between four- and two-pole units is small

A—Six-pole 8,000-rpm unit with 4:1 gearbox
B—Four-pole 12,000-rpm unit with 6:1 gearbox
C—Two-pole 24,000-rpm unit with 12:1 gearbox

Should the system prove suitable, there would seem to be ample scope for further development of these engines, and, in order that it might approach its competitors, it would be absolutely necessary to equip the engines with small highly efficient supercharging equipment.

The authors consider that it is, however, in other directions that the real demand for small engine-driven electric sets ultimately will arise. It is their view that, as a basic unit and having regard to the necessity for duplication, it will be difficult to achieve a specific weight comparable with a main-engine-driven mechanism.

In considering the use of constant-speed mechanisms, a rather interesting point arises—the question of apportioning that part of the weight of the main engine used for generation of electricity to the total weight of the drive. In order to satisfy all interested, this weight has been taken into account mainly on the grounds that small amounts of power lost might affect take-off performance.

It was suggested previously that the demand for auxiliary-generating-plant units probably will be in other directions than that at present being considered. Although it is likely that aircraft of the size considered will operate only from points at which full facilities are provided, nevertheless operating requirements sometimes may call for ground operation remote from the point of fixed supply, and in such a case an auxiliary generating plant for starting and other ground purposes will be called for. The authors have no definite opinion on the subject at this stage, but it is a matter likely to be settled speedily by the aircraft operators themselves.

4. Steam- and gas-driven turbines have been suggested as being convenient methods of feeding prime movers for the driving of alternators in the future. Some work was done in England on schemes of this nature some years ago, but the results of these investigations were not published.

In the case of the steam-turbine system, an exhaust-heat transfer scheme was envisaged, whereas the gas turbine would be driven in a similar way to that now used in the United States for driving engine superchargers. Whether the speed control possible with such a system renders its use satisfactory for constant-frequency a-c generation is not known.

Exhaust-waste-heat energy, however, is being contemplated for a number of additional purposes on aircraft, mainly those of deicing and cabin heating, and it seems questionable whether further energy will be available for the driving of large alternators.

Internal-combustion turbines also have been suggested, but it is not considered that development of this principle has proceeded far enough for serious consideration at this time.

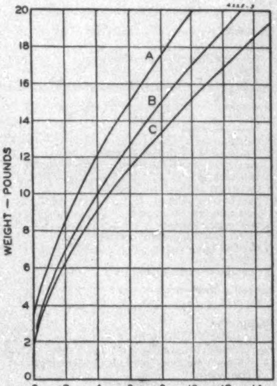

Figure 3. Variation of motor weight with horsepower (continuously rated 400-cycle induction motors)

A—Six-pole 8,000-rpm unit
B—Four-pole 12,000-rpm unit
C—Two-pole 24,000-rpm unit

Having regard to the pros and cons of all these systems, the authors have come to the conclusion that a constant-speed drive should be developed, and that this should form the basis for the generator drive in the type of machine considered.

FREQUENCY

The authors favor a supply frequency of 400 cycles per second for the following reasons:

1. The frequency allows motors and generators to be operated at speeds up to 24,000 rpm which, although hardly practicable today, may well be used in the near future. At such speeds standard bearings with grease lubrication have given satisfactory continuous operation for periods up to 300 hours between regreasing, and this period will be increased when special bearings become available and the method of lubrication improved.

2. Motors of two, four, and six poles may be designed for high efficiency and power factor, but both these figures fall off in motors with more poles, and it is considered that six poles is the economic limit for aircraft motors. This gives a synchronous speed of 8,000 rpm on 400 cycles. The curves in Figures 1 and 2 show that at this speed motor and motor-gearbox weights are tending to increase rapidly for any given output.

3. The weights of transformers, chokes, and capacitors are all reduced by using a higher frequency within the limits envisaged.

4. Skin effect in conductors and eddy current and penetration effects in magnetic circuits do not become severe at frequencies up to 400 cycles.

VOLTAGE

The optimum service voltage is that which with reliability and safety will give the minimum total weight for the whole of the electric equipment. Cable forms a large proportion of the weight, but, as already mentioned, this can be reduced by increasing the voltage above the 12- or 24-volt values that are now common. At voltages above 200–300, the reduction in conductor weight to some extent is offset by the extra insulation required. Investigations carried out in Great Britain suggest that the optimum voltage for equipment such as motors is over 110 volts, though variations between 110 volts and 250 volts appear to have little effect.

It therefore is suggested that power be generated at a line voltage of between 200 and 230 volts, that is, corresponding to 115–133 volts to earth for a star-connected system. Such values are considered quite safe and are less than many used for domestic supplies. It is possible, however, that the effect of electric shock under high-altitude conditions may need investigation.

The authors suggest that agreement between American and British interests on the question of frequency and voltage would be advantageous, since servicing problems and interchange of apparatus would then be simplified.

DETAILS AND CONSTRUCTION OF GENERATING EQUIPMENT

In deciding upon the size of the generating unit to be used on a given aircraft, the following factors should be considered:

1. The reliability of the drive or prime mover employed.

2. The availability of electric services at the termini.

3. The nature of the electric load.

These factors will now be considered:

1. As the reliability of the generating system increases, the necessity for installing separate supplies decreases. From the safety standpoint, however, it is desirable that at least two independent supplies be installed, a point which is amplified later.

2. With electric supplies at all termini, the servicing of the aircraft can be performed without using any of the aircraft main generators, and consequently stand-by plant for such services may not be necessary. As has been stated previously, however, operating experience may indicate that a small auxiliary generating plant should be carried in addition to the main supplies, and this will call for a generator probably of standard installation size or smaller. Likewise, a flying boat may need a larger stand-by auxiliary generating plant than a land airplane, because of the difficulty of readily obtaining shore-based supplies—especially in bad weather.

Table II. Characteristics-Including Specific Weights, of Typical A-C and D-C Generators

1 Output in Kw	2 Volts	3 Amperes	4 Speed in Rpm	5 Weight (Pounds)	6 Weight Per Kw (Pounds)
D-C Generators					
2.85	28.5	100	2,500–5,000	32.5	11.4
5.70	28.5	200	2,500–5,000	42.5	7.5
2.85	28.5	100	4,400–10,000	26.0	9.1
5.70	28.5	200	4,400–10,000	36.0	6.3
8.55	28.5	300	4,400–10,000	46.0	5.4*
Three-Phase 400-Cycle-Per-Second Generators—Excluding Exciters and So Forth					
3	208/120		8,000	17	5.67
6	208/120		8,000	22	3.67
12.5	208/120		8,000	32	2.56
25	208/120		8,000	52	2.07*
50	208/120		8,000	85	1.70*

* Estimated weights.

Comparisons between the weight of a-c and d-c machines are complicated by the many possible characteristics (time ratings, speed, cooling arrangements, and so forth) for which each may be designed. Fair comparison can only be made between machines which are designed for similar services. On this basis, the advantage is definitely with a-c machines, since commutators are usually absent, and the weight saved, and higher speeds thereby made possible enable lighter machines to be designed than for d-c circuits.

3. Electric loads may be classified broadly under two main headings, namely: "Minimum Operational" or those essential to flight and landing, and others not essential to the control of the aircraft. To supply these loads, the authors on an electrical basis only, favor the installation of two larger alternators rather than a greater number of smaller supplies of the same total capacity. From an electrical and weight standpoint, the former system has the following important advantages:

A. The weight per kilowatt is reduced, since in large-capacity generators items such as housing, shaft, and bearings form a smaller proportion of the total weight than in smaller machines. Thus the weight of a 12.5-kw alternator is about 32 pounds, while a 25-kw and a 50-kw generator will weigh about 52 pounds and 85 pounds respectively, exclusive of exciters.

B. A large generator can cope more readily with given shock loads (that is, loads suddenly applied) than a smaller machine, and on large alternators the voltage variation due to such loads is less. This is particularly advantageous with the sectionalized-ring bus system which is described later, and wherein paralleling of supplies is not intended.

C. Circuit layout is simplified.

Such considerations favor the installation of two main generators, but for the sake of additional security it sometimes may be advisable to sacrifice some of the foregoing advantages and install a greater number of main generators on aircraft equipped with more than two main engines. In any case, however, the capacity of any one alternator always should be sufficient to cope with the largest single load classified under "Minimum Operational." The probable load requirements of existing projects therefore suggest the following types of installation:

Two-Alternator System. Each alternator to be capable of providing 50 to 60 per cent of the continuously connected load with a short-time overload capacity of about 30 per cent.

Four-Alternator System. Each generator to provide 25 to 30 per cent of the continuously rated load with a short-time overload rating of about 60 per cent.

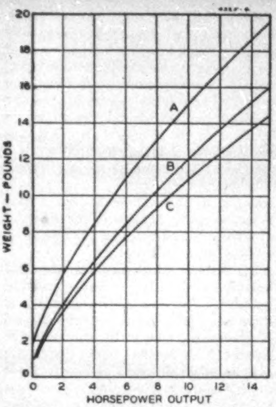

Figure 4. Variation of motor weight with horsepower (90 seconds rated 400-cycle induction motors)

A—Six-pole 8,000-rpm unit
B—Four-pole 12,000-rpm unit
C—Two-pole 24,000-rpm unit

The majority of aircraft alternators designed to date are of the salient-pole rotating-field type. Simplicity and rigidity of construction, together with good natural cooling properties of the field system, give this type of generator advantages over the "turbo" or inductor type. The field system is supplied through two relatively small slip rings which form the only rubbing electric contacts on the alternator.

Blast cooling is employed; for example, up to about 100 cubic feet of air per minute is used for a 12.5-kw 400-cycle-per-second alternator, and 200 cubic feet for a 25-kw machine. A 50-kw generator is expected to need between 300 and 400 cubic feet per minute.

Class-*B* insulation (that is, glass, mica, asbestos) is employed, and current densities up to 10,000 amperes per square inch have been used in the armature conductors and 8,000 amperes per square inch in the field windings. The permissible working temperature is at present limited by the available insulating varnishes to about 150 degrees centigrade.

Flux densities of about 100,000, 87,000, and 53,000 lines per square inch are used for the armature teeth, core, and air gap, respectively.

So far the alternators have been excited by means of directly coupled d-c generators. At present, this method of excitation is preferred to self-excitation schemes, despite the introduction of a commutator and the additional weight of a separate machine. It provides a more stable system and one which will not collapse completely under fault conditions. Voltage control usually has been obtained automatically by varying the field strength of the exciter by means of a carbon-disk-type regulator.

Research work is proceeding on various methods of self-excitation, particularly those using electronic means and dry-plate rectifiers, either with or without the help of shunt or current compounding circuits.

DISTRIBUTION

1. *Alternators and Bus Bars.* It is not proposed at present to parallel alternators, since this introduces unnecessary complications. Also, in the event of a short-circuit on or near the bus bars, larger-capacity switchgear probably would be necessary, because of the large "backup" capacity of the paralleled supplies.

Instead of paralleling, it is proposed that the distribution circuit be divided into as many bus-bar sections as there are main alternators. If two main alternators are installed, then there will be two sections, and the arrangements will be as illustrated in Figure 6. The loads in each section would be balanced as evenly as possible.

When the main alternators are idle, the bus bars automatically will be connected to form a ring bus, from which the main generators then will be isolated. The alternators automatically will be switched in by voltage-operated contactors, which will break the ring bus when they switch the alternators in. The switching in of supplies from an auxiliary generating plant, or from the airdrome, will be similarly controlled, suitable interlocking arrangements being provided. Should the supply from an alternator fail during flight, the contactor automatically will disconnect the faulty generator from its bus-bar section, and switch the latter across that being fed by the other machine.

Such failures would be immediately apparent to the flight engineer, but, if no action were taken by him and the resultant overload were excessive, he would receive a further warning from an overheating indicator when the appropriate temperature was reached.

The change-over switching will be sufficiently fast in practice to prevent the dropping out of essential service contactors.

2. *Feeders.* Feeders to essential services will be duplicated, or in some cases, even more circuits might be used. Distribution boxes used in a similar manner to industrial practice, in conjunction with open wiring, insure the most straightforward method of subdividing the circuits when the load centers are fairly well defined. Flared conduit and similar devices will be needed in certain situations. Solderless connections are preferred and isolating links should be used at situations such as wing breaks, removable panels, walls of pressure cabins, and engine bulkheads.

FAULT PROTECTION

This subject has been given very careful consideration for, while there is no doubt that a-c equipment generally will be more reliable than its d-c counterpart, fault conditions must be taken into account. Faults can be caused by accident or emergency conditions, or perhaps in some cases by incorrect operation by the flight engineer.

Faults can be separated into two main classes:

1. Overheating due to overload or insulation failure, and so forth.

2. Earth (or ground) faults. Phase faults (short circuits). Single phasing.

Table III. Possible Loads for a 100- Ton Civil Aircraft

1	2	3	4	5	6
Continuous Loads			Intermittent Loads		
Service	Kw	Per Cent	Service	Kw	Per Cent of Total Continuous Loads
Ailerons	6.0	8.0	Undercarriage (including doors)	36.0	48
Elevators	3.0	4.0	Wing flaps	25.0	33.3
Rudder	3.0	4.0	Trim tabs	1.0	1.33
Fuel pumps	2.0	2.67	Propeller control	3.0	4.0
Fans and air conditioning	25.0	33.33			
Automatic pilot	1.0	1.33			
Lighting	10.0	13.33			
Heating (including kitchen and buffet)	20.0	26.67			
Radio and instruments	3.0	4.0			
Miscellaneous domestic services	2.0	2.67			
Total	75	100	Total	65	86.63

Total connected load = 75 + 65 = 140 kw.

Connected motor load = 105 kw = 75 per cent of total connected load.

In this case, main generating plant continuously rated at 75 to 100 kw should be installed. In order to allow for the operation of the intermittent loads, the plant should have an overload capacity of 60 per cent for a period of about two to three minutes.

1. Overheating. Certain nonessential services can be arranged to embody built-in thermal protective units and, depending on the duty, can be arranged to be self-restoring or otherwise on cooling. With essential services, it is proposed that visual and/or audible warning of excessive temperature be given by means of built-in thermal devices. This will allow the flight engineer discretion regarding the delay allowable before switching out the defective unit. The alternators themselves should have these built-in devices which allow opportunity for transfer or removal of load before switching out. For very excessive overloads and where rate of rise of temperature is very rapid, the thermal device must operate directly on the protective device. In a few cases it must be possible to force the switch against the thermal protection in order that the motor (such as undercarriage actuator) can be made to complete its function, even at the risk of destruction of the motor in the attempt.

2. Earth Faults, Phase Faults, and Single Phasing. These can be grouped together for the purpose of this discussion. One important requirement is that it should be possible to restore the service as soon as possible after the clearance of the fault, or alternatively, to maintain the service during the fault period. When sufficient backup capacity is available it is sometimes suggested that the fault will "blow clear" without completely interrupting the supply. This means, however, that the protective gear must be of relatively large capacity.

Another method is to limit the fault current as much as possible, and line reactors can be used to limit phase faults.

A method of earth protection which has given very good results in other industries is as follows:

The alternator neutral point is connected to earth via a sensitive high-impedance relay which, in turn, operates a warning indicator.

The occurrence of an earth fault is indicated immediately but, because of the very small current allowed to circulate, (20–50 milliamperes) immediate attention is not required. If, on investigation, it is found that the fault cannot be removed, it can without danger be left to persist until a landing is made. Alternatively, the relay can be disconnected, thereby completely removing the circulating current but moving the earth connection from the neutral point to the point of contact.

Any subsequent earth faults occurring before the first is cleared would rely on the normal overload protection to clear them.

For short-circuit protection, electromagnetic trips represent the best practice. With the present limited experience it is doubtful whether special bus-bar protective schemes are justified.

Unessential-service circuits can be protected by suitable fuses, but trip devices capable of being reset will be used for essential circuit protection.

Generally, all plant and circuit overload devices should be of the inverse-time-current type, and thermal relays therefore are proposed, since they give this characteristic very readily and to a large extent also allow for changes in ambient temperature. They have the advantages of light weight and simplicity and do not have the complication of viscosity changes inherent with the oil dashpot types. They are also more robust than the clock-escapement types.

The earthing of all alternator and motor neutrals as a measure of protection against single-phasing has been proposed. While this method may be worth while in military aircraft, the advisability of its use in civil or transport aircraft is doubtful. First, it is not certain that the major risk in civil aircraft will be from open circuits, especially if multiple-circuit schemes are used. The prior risk, as in industrial systems, would probably be from insulation failure and earth faults. Second, multineutral earthing causes an earth fault to be an excess-current fault, which might make it impossible to keep the affected phase alive. Also, these excess-current faults sometimes are attended by slight structural damage and a fire risk due to the flash and overheating.

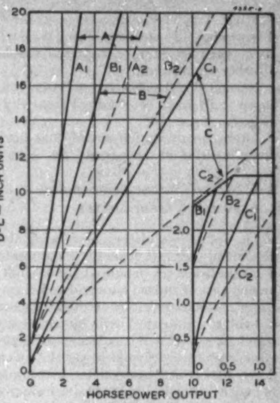

Figure 5. Three-phase 400-cycle induction motors

Variation of D^2L with horsepower
Solid line—continuously rated
Dashed line—90 seconds rated
A—Six-pole 8,000-rpm unit
B—Four-pole 12,000-rpm unit
C—Two-pole 24,000-rpm unit

Table IV. Capacity of Generating Plant Installed in Some Typical British Aircraft

1 Type of Aircraft	2 Date	3 All-up Weight (Pounds)	4 Electrical Generating Plant Installed
Albatross	1937	32,500	Two 500-watt d-c generators
C—class flying boat	1936	40,500	Two 1,000-watt d-c generators
G—class flying boat	1937	74,000	Two 1,000-watt d-c generators
Stirling bomber	1938	70,000	Two 1,500-watt d-c generators
Lancaster bomber	1941	63,000	Two 1,500-watt d-c generators
New flying boat		130,000	One 24-kw (a-c) auxiliary generating plant + 1 spare set
New bomber		77,000	Four 6-kw d-c generators

However, by earthing the motor neutral, a rotating field is provided in the event of one line becoming broken and this gives a pull-out torque of between 40 and 90 per cent in excess of that obtaining with an unearthed neutral—the exact value depending upon the size of the motor. Likewise, a start and run-up torque is produced which may vary up to about one third of the three-phase figure.

The third harmonic which is allowed to flow in a star-connected machine by multiearthing should be minimized by suitable design of motor winding and/or slot combinations—otherwise there may be a tendency for the induction motors to crawl. The greater losses also may cause undue heating, and high-rated motors may not then have sufficient margin of power or temperature rise to operate under emergency conditions. Any out-of-balance load on the alternators causes some displacement of the neutral point which, in turn, causes a general circulating current to flow.

Summarizing, the advantages of solid multiearthing, and relay earthing, together with the probability of single-phasing faults and occurrences of earth faults all have to be balanced against each other. Only experience will indicate the best system, but meanwhile the authors favor relay earthing.

Operating coils for short-rated contactors can be arranged for a-c operation, but continuously rated coils are most economically operated by direct current obtained by means of suitable rectifiers located near the coils. All major services can be push-button-operated from panels located in the flight engineer's cabin.

D-C Supplies

Certain d-c supplies still will be required for services such as emergency lighting and fire-extinguisher control. These may be obtained from a small trickle-charged storage battery, should all other supplies fail.

Normal d-c supplies for radio or contactor coils may be obtained from dry-plate rectifiers.

Alternator excitation already has been covered earlier in this paper.

A-C Motor Characteristics and Associated Gear Drives

The induction motors now being developed for aircraft generally incorporate a laminated rotor mounted on a shaft, having cast or brazed bars and end rings; stator laminations either riveted together or held in a thin steel cylinder with ends rolled over; class-*B* insulation throughout except for leatheroid in the slots as a purely mechanical protection. (Attempts to omit the slot insulation and use simply varnish-sprayed slots have met with some success.)

Intermittently rated motors usually are totally enclosed and continuously rated machines fan-ventilated and blast-cooled. In general, continuously rated motors should have a pull-out torque about equal to $2^1/_2$ times full-load torque, and a starting torque of at least ten per cent above full-load torque. Short-time-rated motors should have a starting torque equal to about 110–120 per cent of full-load torque and, by adopting special rotor-slot shapes, this value can be maintained nearly constant from standstill to 20 per cent slip. Figure 5 shows a curve of motor D^2L against horsepower for continuously rated and short-time-rated 2-, 4-, and 6-pole designs. It now has become apparent that the ratio of D/L may be varied over a wide range (from about 0.7 to 1.2) without greatly affecting weight, thus permitting considerable latitude in design.

Figures 3 and 4 show the estimated weights of some 2-, 4-, and 6-pole 400-cycle induction motors, continuously and intermittently rated. Figures 1 and 2 show the weights of motor-gearbox units, in each case arranged to give an output speed of 2,000 rpm. Other output speeds can be selected for comparative purposes. In these instances it will be noticed that the over-all reduction in weight due to the adoption of a 24,000-rpm motor speed instead of a 12,000-rpm motor speed is small, since the effect of gearbox weight must be taken into account when considering the use of high motor speeds.

Plant and Load Requirements

The capacity in kilowatts and the weight of the generating plant installed, will depend mainly upon the load factor (that is, ratio of average to maximum load) and duration of the peak load. The capacity generally will increase with the size of aircraft, and some details of the latter are needed before the generating requirements can be estimated reliably.

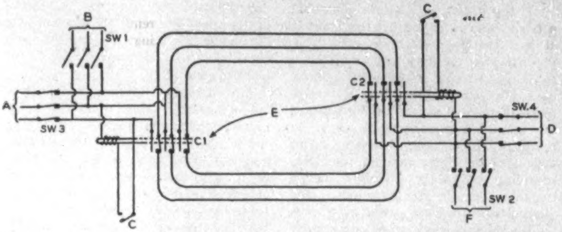

Figure 6. Ring-bus system to insure continuity of supply

This scheme is applicable to any number of alternators

Switch *SW1* interlocked with *SW3*, and *SW2* interlocked with *SW4*

A—From alternator 1
B—Alternative supplies from airdrome
C—Panel switch
D—From alternator 2
E—Double-throw contactors
F—Alternative supplies from airdrome or auxiliary generating plant

The estimated loads for a 100-ton land airplane are given in Table III, from which it will be seen that a large proportion (70 to 80 per cent) of the power generated is allocated for the driving of induction motors. The maximum connected load is 140 kw, of which 75 kw is rated as continuous, but it is unlikely that all this connected load will be in circuit at the same time. In this instance, therefore, the main generating plant should have a continuous rating of about 75 kw with an overload capacity of about 60 per cent for about two or three minutes. This would amply cover the requirements of the intermittent loads.

Conclusions

In this paper the authors have attempted to record in broad outline their views on the most suitable form of power supply for operation of services in large aircraft; they may be summarized thus:

1. Services should be operated by means of three-phase a-c power generated at a frequency of 400 cycles per second at a line voltage of 200–230. For various reasons such a system is preferred to a medium-voltage d-c system.

2. On a purely electrical basis generators should be of the smallest number (not less than two) and consequently of greater individual size, depending on the load requirements, but other considerations such as reliability and division of driving power required may and probably will modify this conclusion.

Thus for a four-engined aircraft requiring 100 kw of power for optimum electrical performance, two generators of say 60 per cent total load capacity should be provided, but other considerations probably would vary this arrangement in favor of four generators, each of say 30 per cent full system load capacity.

3. There should be a short-time overload capacity of about 60 per cent in respect to each individual machine in a two-alternator system and of about 30 per cent in the case of four alternators. Paralleling at this stage is unnecessary.

4. The constant frequency and voltage required are obtained best by means of some form of constant-speed drive from the main aircraft engines.

5. It may be necessary to carry a small auxiliary generating plant for ground operation on some types of aircraft or on certain routes.

6. Power-driven services should be operated in the main by high-speed induction motors with associated transmission apparatus where required. The speed should be chosen so that when used in conjunction with additional apparatus the lowest weight is achieved.

7. A simple sectionalized ring bus is preferable for aircraft bus-bar systems, the number of sections being equal to the number of generators installed.

8. Feeders, which should be duplicated where possible, should be assigned to each ring-bus section with a view to obtaining the best possible power balance from the complete system.

9. Overheating should be dealt with by thermal overload devices with provision for maintaining an affected unit in circuit where required.

Several forms of protection are put forward for other types of fault, and these are described in detail.

10. D-c supplies may be obtained from metal-plate rectifiers.

The authors regret that shortage of time and prevailing conditions have made it difficult for them to cover the subject as fully as they would have liked, but they hope that these notes will be interesting to American engineers as an expression of opinion on the lines upon which the larger electric systems will develop in Great Britain and a statement of some of the progress which has been made in that country.

Design of an Ignition System for an 18-Cylinder Aircraft Engine

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THE designer usually starts a new design by asking himself this question: "What features are absolutely required in this product for it to fulfill its function?" After this question is answered, he makes a preliminary design and often builds a model to see if the basic requirements are met. After they have been met, he asks himself a second question: "What additional features can be incorporated in this product to make it outstanding?" The final design usually, therefore, contains some features from the first category, which can be called "design requirements" and some features from the second category, which can be called "design improvements."

The design requirements for an aircraft-engine ignition system are:

1. It must generate a high enough voltage to spark at the spark plug.
2. This spark must occur at the proper time.
3. This spark must be available from minimum cranking speed to maximum dive speed.
4. The ignition system must fit the engine and airplane.
5. The ignition system must endure and operate under all the conditions to which the aircraft is subject and those additional ones which it creates.
6. The ignition system must not interfere with other functions of the complete aircraft.

The design improvements which should be built into an ignition system are:

1. It must be lightweight and have small bulk.
2. It must be easy to install and service.
3. It must require a minimum of maintenance.
4. It must be easily manufactured by mass-production methods.
5. It must have a good physical appearance.

These design requirements and improvements will be enlarged upon and the methods for meeting the requirements and making the improvements will be discussed. The electrical design of the ignition system will then be discussed in detail for the benefit of those readers who are interested in actual ignition-system design.

It must generate a high enough voltage to spark at the plug. This voltage is determined by the power output of the engine and the spark-plug gap (between electrodes). The magneto is connected to each plug by a cable running through a grounded shielding conduit. The high-voltage conductor therefore has a certain electric capacitance to ground. In order to raise the voltage of the spark plug to the firing point, the magneto must supply enough energy to charge this capacitance to the breakdown voltage of the plug and to supply the energy losses.

Several of the energy losses in the ignition system are at high voltage. The causes of these losses include shunt resistance due to fouled spark plugs, electric leakage in the distributor, and corona losses at high altitude. Other high-voltage losses are voltage drop at the jump gap distributor and resistance voltage drop in the high-voltage winding of the coil.

The low-voltage losses in the magneto include the energy required to charge the low-voltage capacitor; copper losses in the low-voltage winding; and hysteresis and eddy-current losses in the laminations, magnets, and other conducting parts of the magneto adjacent to the magnetic circuit. There are also mechanical-energy losses in the bearings, windage, and breaker mechanism.

The detailed electrical design required to supply sufficient energy to operate at the required voltage and to supply the losses will be discussed later. The mechanical losses have been kept at a minimum by using ball bearings and eliminating all internal gearing.

This spark must occur at the proper time. The time at which the spark occurs is controlled primarily by a breaker and distributor. The breaker controls 18 sparks so that they occur at the proper intervals, and the distributor routes each spark to the proper cylinder. The 18-cylinder ignition system described consists of two magnetos which spark simultaneously as there are two spark plugs per cylinder.

The over-all accuracy of timing must be within about ± four degrees of crankshaft revolution. This error includes gear backlash in the engine, timing error between the engine and magneto, error of adjustment of magneto internal timing, delay due to electric charging of harness capacitance, variations in breakdown voltage in the spark plug, and manufacturing tolerance in all the parts. This error must also include wear on the various parts during the operating period between overhauls. This is a total variation in timing from all causes of plus or minus approximately 0.0003 second.

This is somewhat remarkable, considering that timing is obtained by completely mechanical means and that the timing mechanism, the cam and breaker, opens and closes the circuit up to 600 times per second. During the life of a set of breaker contacts it will open approximately 1,000,000,000 times. An error of one degree in timing is caused by approximately $^{1}/_{1,000}$th of an inch wear of the breaker contacts or the cam follower. Accuracy depends primarily therefore on the elimination of contact and follower wear. It happens that contact wear advances the spark, whereas follower wear retards the spark, and so a balancing of these two rates of wear will minimize shift in timing. In the ignition system described, a very small-diameter cam has been used to lower cam rubbing speed. The moving parts of the breaker arm and contact have been made as light as possible in order to reduce the inertia loading and cut down the spring tension required to keep the follower against the cam. Because of the large number of operations per second, the follower acceleration is quite high, being about 300 times the acceleration of gravity, maximum.

The time delay during which the magneto is charging the harness to spark-plug voltage is quite variable. This time depends on the inductance of the magneto high-voltage coil, the capacitance of the harness, the magneto energy, and the breakdown voltage of the spark plug. The system described uses a low-inductance high-voltage coil in order to reduce this charging time. This delay can amount to two degrees of crankshaft revolution and can vary over one degree from one spark plug to the next. A reduction in the capacitance of the harness (high-voltage cables to the spark plugs) also will decrease this delay. We have kept the capacitance of the harness small by means discussed later in this paper.

The interval from one spark to the next is not constant for radial engines. This is required in radial engines using one master rod in each bank, because the pistons in the remaining cylinders are connected to the master rod by link rods, rather than directly to the crank pin. This causes some of the pistons to come to top dead center before the crank pin crosses the radial axis of the cylinder and some of the pistons to come to top dead center after the crank pin crosses the axis of the cylinder. In an engine with more than one bank, for instance the 18-cylinder engine with two banks, the

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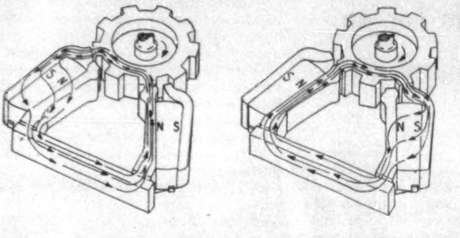

Figure 1. Magnetic circuit of 18-spark inductor-type magneto shown with rotor in two different positions

cylinders containing master rods must fire with some angular spacing between them. Consequently, some cylinders fire with positive (advanced) compensation directly after cylinders having negative compensation.

The cam must take into account this "compensation" of each spark. It has unequally spaced lobes and must revolve at the cycle frequency of the engine, or one-half crankshaft speed. It must be properly synchronized to the engine and distributor as well as the magnetic circuit, to give the correct compensation to each spark.

Since the synchronization of the cam and breaker to the magnetic circuit and to the distributor is so critical, the breaker mechanism cannot be adjusted to synchronize the magneto to the engine. A differential drive coupling must be used. In the system described a vernier adjustment is obtained by using a gear and a spline, or two splines, both of which can be adjusted simultaneously. The minimum adjustment then becomes a very small fraction of a revolution (about one degree maximum).

This spark must be available from minimum cranking speed to maximum dive speed. The design of the magneto to generate voltage at very low speeds is discussed later in the electrical-design section. In some cases, where extremely low cranking speeds are encountered, such as in the Arctic regions, a tremendous overdesign factor would be required to generate sufficient voltage for starting. It is for this reason that most airplanes are provided with a "primary booster." This is a vibrator which sends pulsating current from the airplane battery through the low-voltage winding of the magneto. The magneto then functions purely as a transformer. While the breaker contacts are closed, no voltage is induced in the high-voltage coil, but when the breaker contacts are open high voltage is induced, and it is distributed by the magneto distributor. In half the sparks this current is in opposition to the permanent magnets in the magneto. Precautions must be taken in the design and use of the ignition system to keep this "booster" current from damaging the magneto. Provision of heavy low-voltage wiring and plenty of leakage flux across the magnets prevent damage to the ignition system described.

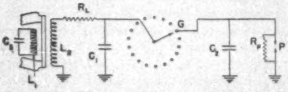

Figure 2. High-voltage circuit of ignition system before distributor gap breaks down

C_s—Low-voltage capacitor
L_1—Inductance of low-voltage winding
L_2—Inductance of high-voltage winding
R_L—Distributed resistance of high-voltage winding
C_1—Distributed capacity to ground of high-voltage parts in magneto
G—Distributor gap
C_2—Capacity to ground of harness lead
P—Spark-plug gap
R_P—Shunt resistance of fouled plug

At diving speeds there are both electrical and mechanical problems. The electrical problems will be discussed later. The mechanical problem is one of inertia of breaker parts. Any bouncing of the breaker follower will give either a spark at the wrong time or a weak spark. The acceleration has been kept as low as possible, and all moving breaker parts have been made as light as is consistent with strength. The complete magneto often is subjected to very high vibration acceleration, so that an excess of spring tension must be provided to keep the follower from leaving the cam.

The ignition system must fit the engine and airplane. The ignition system for an 18-cylinder engine must connect to the engine in about 60 different places. There are 36 spark plugs to connect to. The spark plug leads emerge from the manifold as far as 30 inches from the spark plug, and these long leads must be supported en route and also pass through holes in the cooling baffles. Both magnetos mount on drive pads on the nose of the engine and must be designed to clear governors, oil piping, shrouds, cooling blowers, propellers, push-rod covers, the nose itself, each other, and the harness manifold.

The ignition system suffers from the same problem faced by all accessories on the engine—that is, it must be built under and around and through and over the rest of the engine parts and accessories. It covers the engine very much like an octopus with 36 tenacles. The number of different lead lengths must be kept to a minimum from a service standpoint; yet there are four different spark-plug positions and many oil pipes, propeller controls, deicer pipes, baffles, and so on, to clear.

There is usually only one control connected to each half of the dual-ignition system. This is the ground lead which is used to turn off ignition. Some magnetos also have a differential coupling between the engine and magneto or magnetos to give manual or automatic control of the spark advance angle. These controls must emerge from the ignition system at suitable locations for connections to the control means.

There seems to be only one way to make the ignition system fit the engine and airplane. That is a more or less continuous process of design, try, and cut. Airplane and engine designs are by no means static, and so this is truly a continuous process. It is only by recognizing it as such that an ignition-system designer can keep up with the engine and airplane.

The ignition system must endure and operate under all conditions to which the aircraft is subject and those which it creates. Various parts of the ignition system operate between temperature extremes of minus 100 degrees Fahrenheit and plus 250 degrees Fahrenheit. The breaker pivot sleeve must be lubricated, for example, for operation between minus 50 degrees Fahrenheit and plus 250 degrees Fahrenheit. This requires the use of an oil-impregnated porous bronze sleeve and very close control of the sleeve to journal clearance. This bushing is being lubricated with "Unavis" number 48 oil and a diametral clearance of 0.0004 to 0.0007 inch is obtained by selective assembly. The upper ball bearing on the shaft must be packed with a grease which will lubricate and yet stay in the bearing over this temperature range.

The design of the housing and air gap of the magneto are influenced by the operating-temperature limits. The mag-

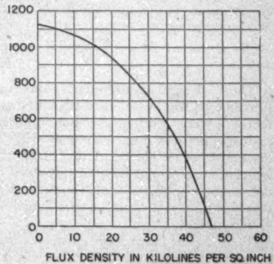

Figure 3. External magnetomotive force for Alnico II

netic structure must be steel, and the housing cannot be steel and must therefore be aluminum or magnesium. This means that differential expansion between the magnetic circuit and the housing will cause the air gap to vary. It is desirable to have as small an air gap as possible, and so this differential contraction at low temperature becomes a limiting factor. To this are added clearances, tolerances, and allowances in bearing wear to give the actual-design air gap at room temperature.

Flight at high altitude requires that the ignition system function perfectly in an atmosphere with about one fifth of the density at sea level. This presents two problems—flashover and corona loss of energy. To prevent flashover, all points of high voltage are separated as far as possible from all other metallic parts, either at ground potential or otherwise. In the distributor this is a dynamic separation, as the high-voltage point on the distributor rotor is moving. This requires the designer to move each distributor electrode as far as possible from any position of the distributor rotor except the one which is required to jump to that electrode. This necessitates a balance of three factors in the positioning of each distributor electrode, and results in a spacing of distributor electrodes which is neither uniform nor compensated the same as the cam.

To prevent corona loss, all high-voltage conductors and connections must be surrounded by solid dielectric of as great a thickness as possible, and all exposed points of high potential should have curved surfaces of large radius. In the ignition system described, the high-voltage cables are insulated with rubber to a diameter of five millimeters, and these insulated cables are embedded in a semisolid flexible dielectric material. This effectively prevents corona losses except in the distributor itself.

To make provision for flight to even greater altitudes, the magneto has been designed as a hermetically sealed unit so that it can be pressurized from the turbosupercharger or an auxiliary pump. To provide ventilation under these conditions a metering orifice in each magneto housing allows a certain flow while maintaining sufficient air density to prevent flashover and minimize corona loss.

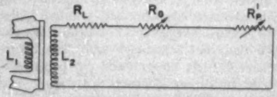

Figure 4. Circuit of ignition system after spark-plug gap breaks down

L_1—Inductance of low-voltage winding
L_2—Inductance of high-voltage winding
R_L—Distributed resistance of high-voltage winding
R_G—Resistance of arc at distributor gap
R_P'—Resistance of arc at spark plug electrodes

Many airplanes operate under severe moisture conditions. Each part of the ignition system must be made of a material and design which is the least affected by moisture. Those parts which would be affected by moisture must be protected. The harness-manifold and spark-plug leads are filled completely with solid dielectric to prevent the entrance of moisture, gasoline, or oil. The wires from the distributor to the harness are in a channel filled with solid dielectric, and the plugs between the magnetos and harness are designed of synthetic rubber which is compressed and completely fills the connection, leaving no space for the entrance or trapping of moisture.

The distributor rotor, shield, and coil are made from a melamine formaldehyde plastic which is nonporous, and track-resistant. The coil itself is vacuum-impregnated with a solventless varnish to eliminate all voids and then is sealed inside the plastic case by a flexible semisolid dielectric material which prevents entrance of moisture.

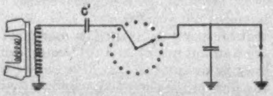

Figure 5. Ignition system using series capacitor C′ to decrease time constant of high-voltage circuit

The metal parts of the ignition system are designed of aluminum, stainless steel, and monel, wherever practical, and all carbon or alloy steel parts inside the magneto except the magnetic circuit, bearings, shaft, and cam are nickel-plated. These materials have been found to withstand best the highly corrosive atmosphere that is caused by fixation of nitrogen in the distributor spark. This condition is very severe, and any unplated brass parts would turn bright green in an unventilated magneto in 12 hours. Ventilation helps prevent this condition, and so about five vents screened by monel are used in each magneto. These vents are located so that any water which collects in the magnetos will drain in any position or flight aspect of the airplane.

Vibration is especially severe on aircraft engines, as had already been mentioned with relation to breaker bounce. All parts must be designed with this in mind and adequately engine-tested and flight-tested with all possible engine–airplane–propeller combinations, before they can be declared free from vibration troubles. The most important consideration in design of parts subject to vibration is to avoid structures or parts having low-resonant frequencies. On most aircraft engines this means avoiding resonant frequencies under 200 cycles per second. Where the resonant frequency must be low, as in the design of spark-plug leads, it should be quite low, and the damping should be as large as possible. Cantilever support of anything but the stiffest kind of parts should be avoided even at the expense of increased weight.

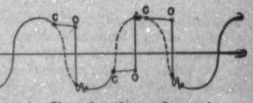

Figure 6. Plot of coil-core flux with time showing variation in total flux change with variation in breaker opening and closing points on flux wave due to compensation

C—Breaker points close
O—Breaker points open

The ignition system must not interfere with other functions of the complete aircraft. In the past, the ignition system has been responsible for a large share of the interference of the engine with radio reception in the airplane. Effective radio shielding of the ignition system is obtained only by enclosing the complete system in a continuous-conducting container. This conducting container must be visualized as a fluid-tight vessel, containing electromagnetic radiation of all radio frequencies.

To accomplish this effective radio-interference elimination, a high-quality liquid-tight flexible metal hose is used on the spark-plug leads. This metal hose is composed of spiral-convoluted metal tubing surrounded by a tightly woven metal braid. Two types of harness manifold are used. One is a continuous cast hollow aluminum ring. The other is a combination cast and tubular manifold which consists of three brass tubular sections and two cast aluminum sections where the manifold is connected to the magneto. The connections between the tubular and cast sections are made by spherical spring-loaded collet-type all-metal joints and give cylindrical symmetry to flow of high-frequency current.

Connections between the magnetos and the manifold are metal to metal to minimize tolerance build-up, and contain a neoprene-loaded flexible metal-ring gasket for radio-shielding continuity. An extension of the neoprene ring gaskets the two parts to exclude moisture. The magnetos themselves are made with as few joints as possible, and these joints all have metal-to-metal contact for dimensional continuity, and neoprene-loaded metallic gaskets for radio-shielding continuity. Aluminum has been used in preference to magnesium because of the more durable electrically conducting surface that can be obtained on aluminum.

In the near future radio filters will be installed in the grounding leads to prevent the flow of high-frequency current into the rest of the airplane's electric system via the primary booster. Radiation from this lead can be prevented by the use of the high-quality conduit now used on the spark-plug leads.

The ignition system must be lightweight and have small bulk. These go hand in hand and are accomplished in the described design by grouping all magneto components on one shaft, in one housing. Both magnetos could be built in one housing, but this probably would be going too far, from the safety standpoint.

The use of a large-diameter short-cantilever-length thin-walled stressed housing gives strength and rigidity without excessive weight. The harness manifold is partly supported by the magnetos to eliminate the complication of a flexible joint. By this means the same members are used for support, shielding, and protection. To reduce effectively over-all weight it is advisable to make a policy of considering no part too small for redesign from a weight-saving standpoint.

The ignition system must be easy to install and service. This is equally important in wartime or peacetime service. In the described system each magneto is individually detachable without disconnecting any high-voltage wires. This is accomplished by the neoprene-insulated air-excluding high-altitude detachable radio-shielded plug already mentioned. Each spark-plug lead is individually detachable from the manifold as a unit, still containing its conductor. The connection at the manifold is made by a spring contact and is gasketed against moisture and insulated for high altitude.

The right- and left-hand magnetos are completely interchangeable, so that only one kind of magneto has to be carried in stock. The number of spark-plug lead lengths has been reduced to four to ease the problem of stocking spare parts. When an airplane covers several thousand miles per day, many different stock piles must be maintained. The number of different spare parts is therefore important.

The four spherical joints in the harness manifold already have been mentioned. When these joints are loosened, the sections of the manifold are free to rotate, translate, and bend at this joint. This extreme flexibility allows the manifold to be installed over the propeller and various accessories with ease, and yet, when the gland nuts are tightened, the manifold becomes a rigid supporting tubular beam.

Access to all parts of the magnetos usually requiring servicing is obtained by removing two covers. One cover exposes the distributor, shield, cam, and breakers, timing lines on magnetic circuit, and engine hold-down bolts. The other cover exposes the coil, magnetic circuit, and ground connection. The complete rotating element of the magneto is removed by unscrewing the nut holding the splined drive gear and the four screws on the breaker plate. When the magneto is removed from the engine, the rubber plugs between the magneto and the manifold are exposed. These plugs are removable, and contain the springs which make electric contact.

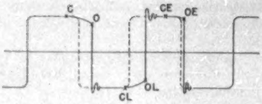

Figure 7. Plot of coil-core flux with time showing that "compensation" will have little effect on a magneto with rectangular flux wave

C—Breaker closes
O—Breaker opens
CL—Breaker closes late
OL—Breaker opens late
CE—Breaker closes early
OE—Breaker opens early

The ignition system must require a minimum of maintenance. This means that the parts and materials used must not wear out quickly and that the system must be designed so that little service is required on the parts and materials that are used. One of the advantages of the half-crankshaft-speed drive is that it gives long bearing and shaft seal life and eliminates gearing in the magneto.

Tungsten is used on the distributor-rotor sparking surface, and monel is used on the stationary electrodes. These materials have a very low rate of erosion, and so they will last the life of the magneto without replacement. This also simplifies the design in that no provision for replacement is required.

The upper ball bearing slides in a hardened stainless-steel bushing. This prevents scuffing corrosion at this point due to planetary rotation of the outer bearing race and vibration pounding.

Platinum iridium contact points are used to provide stable breaker operation. The small-diameter cam and lightweight cam follower minimize maintenance of the breaker mechanism. Much effort has been applied to the proper lubrication of the cam and follower.

The use of stainless-steel hardware wherever possible and adequate ventilation and drainage reduce replacement of screws, nuts, and washers. An effort has been made to keep the number of screw and nut sizes to a minimum. This allows the mechanic to provide more easily the proper tools for each operation and consequently minimizes mutilation of hardware during overhaul.

Solid filling of the magneto and harness cable conduits has already been mentioned. This reduces maintenance by preventing erosion due to chaffing of the wires over cast surfaces and sharp corners. At the high-vibration levels encountered on aircraft engines this chaffing is often quite bad on unfilled harnesses. The solid filling of the coil also prevents shifting of the wires and fatigue of the insulation from corona.

The ignition system must be easily manufactured by mass-production methods. In the ignition system described, the number of milling operations has been reduced by making all surfaces concentric wherever possible. Handling has been reduced by concentrating most of the machining of the magneto in the one-piece housing. Intersecting parting lines have been eliminated because of the bad tolerance and interchangeability problems they present.

Die castings and molded plastics are used wherever possible to reduce the amount of machining. The two covers are die cast, and all except the smallest plastic parts are molded. Insert design of the plastic parts has been improved constantly to reduce losses due to cracking. It has been found that inserts should be smooth, round, and small, but self-supporting in order to be molded successfully into melamine formaldehyde plastics.

Selective assembly is only used between parts which will never be disassembled, such as the breaker pivot and bushing. The design should require little machining after assembly, since many man-hours are lost if any trouble is encountered in these subsequent machining operations. In order to reduce the cost of the coil, several high-voltage windings are gang-wound at the same time, and the individual coils are sawed apart and then assembled over the primary windings. This allows the coil to be impregnated with solventless varnish as a unit and then assembled into the case. Spoilage cost due to poor impregnation therefore does not include coil case and connections.

The ignition system must have a good physical appearance. An aesthetic design can be accomplished by avoiding concave or re-entrant surfaces in the design and by making the external surface as smooth and curved as is consistent with functional design. Transitions from the engine should be made to "flow" as smoothly as possible, as this is also good design from a strength standpoint. All unmachined surfaces are painted black to reduce the effect of surface irregularities, and machined and plated surfaces are left unpainted. All parts subject to direct contact with tools are plated rather than painted, as paint is invariably knocked off these surfaces and soon gives the system a poor appearance.

Electrical Design of 18-Cylinder Ignition System

A magneto is a hybrid machine. It is neither a sine-wave a-c generator nor an induction coil, but has some of the characteristics of each. For simplicity it can be treated as an a-c generator with a transient load.

Generally a magneto produces one spark for every flux reversal through the armature, or two sparks per cycle. Most magnetos produced in the United States are two-, four-, or eight-pole, producing two, four, or eight sparks per magneto revolution. It therefore has been the practice of magneto designers to drive the magneto generator through gearing to supply the proper number of sparks for a multicylindered engine. For example, on a nine-cylinder engine, requiring $4^1/_2$ sparks per revolution, a four-pole rotor is used with a 4-to-$4^1/_2$-gear ratio step-up. The distributor rotor for a four-cycle engine must run a one-half crankshaft speed, and so, unless the magneto generator produces the same number of sparks as there are cylinders, there must be gearing between the magneto generator and the distributor. A magneto with a compensated cam also must have gearing between generator unit and cam unless the generator unit produces as many sparks per revolution as the engine has cylinders. It therefore is highly desirable to design an 18-spark magneto generator into an 18-cylinder magneto, as generator, distributor, and cam can all be on the same shaft.

Because of the high voltage at which the magneto operates, it is not practical to use a distributed armature winding. The armature winding usually is wound around a single flux path in a position where it can be well insulated. If the magneto is designed with a rotating field of 18 poles, only two poles will be in use at any one time, and the magnetic circuit will be very inefficient in use of weight and space. An inductor-type generator therefore is used. This uses each magnet half the time and about 70 per cent of the magnetic circuit all the time. The magnetic circuit of the 18-spark inductor magneto generator is shown in Figure 1.

To operate as an induction coil, the magneto must have a low-voltage winding and a high-voltage winding, with essentially the same magnetic circuit. They are therefore both wound on a laminated-iron core, with the low-voltage winding next to the core. In operation the breaker contacts close, short-circuiting the low-voltage winding. The generator action of the magneto then causes a current to flow in this winding. At the proper time this current is interrupted by opening the breaker. The accompanying rapid change of flux generates the required voltage in the high-voltage winding.

Although this Doctor-Jekyll-and-Mr.-Hyde illustration of the two roles of generator and induction coil serves to illustrate the operation of the magneto in general, a design analysis on this basis would be rather cumbersome. It is somewhat easier, therefore, to visualize the operation in three steps as follows:

1. The magnetic circuit is in a position to send flux through the coil core in one direction. At this time the breaker closes, short-circuiting the low-voltage winding.

2. The shaft is rotated so that the magnets attempt to send flux through the coil core in the opposite direction. The flux through the coil core tends to remain constant, however, because of the short-circuited low-voltage winding. A current therefore is set up in this winding.

3. The breaker contacts open, interrupting the current in the low-voltage winding. The flux can now reverse, generating a voltage in the high-voltage winding.

Based on these three steps, an analysis of the magnetic- and electric-circuit requirements can be made. First, the question of "how much voltage" must be settled. This depends on the following factors:

1. Spark-plug electrode spacing—may vary from 0.008 to 0.035 inch; usually between 0.012 and 0.025 inch.

2. Partial density of air in cylinder at the time spark occurs: present maximum about seven tenths of a pound per cubic feet.

3. Ratio of fuel to air in combustible mixture.

4. Other factors contributing to ionization, such as shape of spark-plug electrodes, radiation from hot spots in cylinder, and vapor pressure of internal coolant.

These factors will vary for every engine, and so the ignition-system design also must vary according to the engine on which it must operate. Current engine design requires a maximum voltage of 12,000 volts if the spark-plug gaps are held to a maximum of 0.025 inch.

Since a magneto does not produce energy at a constant rate, but rather in pulses, its output is stated more clearly in terms of the energy in each pulse, usually referred to as the energy per spark. Actually, a very small amount of energy is required to ignite the mixture. The electric energy produced by the magneto is practically all dissipated as heat at one part of the cycle or another. Since we are interested only in producing a spark, what happens after the spark occurs is only of minor interest. The distribution of energy before a spark occurs is as follows:

1. The energy required to charge the capacitance of the harness and all high-voltage parts of the magneto to the breakdown voltage of the spark plug = W_1.

$$W_1=\frac{1}{2}(C_1+C_2)E^2 \text{ (see Figure 2)}$$

2. The energy dissipated in shunt resistance at the spark plug because of fouling = W_2

$$W_2=\frac{1}{R_P}\int_0^t E^2dt \qquad t = \text{ charging time}$$

If rise of voltage is sinusoidal with time,

$$W_2=\frac{E^2t}{2R_P}$$

3. The energy dissipated in the spark at the distributor = W_3 (see Figure 2)

$$W_3=\frac{C_1E_q^2}{2}\left(1-\frac{C_1}{C_1+C_2}\right)$$

where Eq = breakdown voltage of distributor gap.

4. The energy stored in the low-voltage capacitor = W_4

$$W_4=\frac{1}{2}C_3E^2\left(\frac{n_1}{n_2}\right)^2$$

n_2 = number of high-voltage turns, n_1 = number of low-voltage turns.

5. The energy lost in hysteresis, eddy-current, and insulation losses when the breaker opens and the flux reverses = W_5. This amounts to about 50 per cent of the energy in current in the low-voltage winding before the breaker opens.

$$W_5\approx\frac{1}{4}L_1i_1^2$$

The energy which must be available as current in the low-voltage winding, therefore, just before the breaker opens is W where

$$W=W_1+W_2+W_3+W_4+W_5$$

$$W\approx 2(W_1+W_2+W_3+W_4)$$

$$W\approx(C_1+C_2)E^2+\frac{E^2t}{R_P}+C_1E_q^2\times$$
$$1-\frac{C_1}{C_1+C_2}\Big)+C_3E^2\Big(\frac{n_1}{n_2}\Big)^2$$

This is the energy which would have to be available in the primary winding of an induction coil if straight-coil ignition were used. Now consider the magnetic circuit: this energy must now be available as flux stored in a magnetic circuit with reluctance. The energy stored in a magnetic circuit is

$$W=\frac{R\Phi^2}{2}\times 10^{-8}$$

where Φ is the total flux change necessary to release the energy. In a magneto the total flux change is twice the maximum flux through the coil, Φ_c, at the time the breaker closes. Therefore, if $\Phi = 2\Phi_c$, then the energy available when the breaker opens = W

$$W=R\frac{(2\Phi_c)^2}{2}\times 10^{-8}=2R\Phi_c^2\times 10^{-8}$$

R is the mutual reluctance presented to any magnetomotive force at the coil core. It is the ratio of magnetomotive force to flux in the coil core. This reluctance can be calculated as a first approximation by adding the reluctances of the two series air gaps and the reluctance of one magnet, based on a permeability of six for Alnico II, and neglecting leakages.

The required flux Φ_c in the coil core when the breaker closes now can be calculated. This flux is due to the magnetomotive force of the magnet. This magnetomotive force sends flux both through the coil core and through the leakage paths. In an inductor magneto the leakage flux is a large percentage of the total flux—in the described design, about 60 per cent. If ρ is the ratio of useful flux to total magnet flux, the total magnet flux (at the time of maximum flux through coil core) is Φ_e/ρ and the flux density in the magnet is $\Phi_e/(\rho \cdot A_m)$.

A calculation of the magnetomotive force of magnet required to give a certain flux through the coil requires a knowledge of the mutual reluctances of the magnetic network, and there may be eight to 12 different reluctances to consider. In making a completely new design, it is necessary first to flux-plot the proposed design and estimate these various reluctances. Then, from a network analysis, the required magnetomotive force can be found. When a modified design is made, the reluctances can be estimated from the previous design. It has been found that a convenient figure to use in design calculations is the mutual reluctance between the magnetomotive force and the coil-core flux, *Rmc*. In the described magneto, this is roughly twice the self-reluctance of the coil core. Once this mutual reluctance, *Rmc*, is established, the required magnetomotive force of magnet (*F*) can be calculated.

$$F = \Phi_e Rmc$$

The external magnetomotive force of a permanent magnet depends on the flux density in the magnet because of its high internal reluctance. This is similar to the external voltage regulation of a battery due to its internal resistance. Since internal magnetomotive-force drop is of no value, the external magnetomotive force usually is used in calculations. The internal magnetomotive force (and hence also the external magnetomotive force) depends on the degree of magnetization, of the magnet. If it is magnetized to saturation (roughly 75,000 lines per square inch), it has the greatest internal magnetomotive force, but, since it cannot be used at this flux density, the flux density must decrease, and as it does the internal magnetomotive force decreases.

The external magnetomotive force of the magnet depends, therefore, on the minimum flux density to which it is ever subjected, and the external magnetomotive force at this flux density is the value on the major hysteresis loop (see Figure 3). In the magneto described, this is a flux density of about 6,000 lines per square inch and occurs when the magnet cell is removed from the magneto. When the magneto is assembled and the rotor placed in the position of maximum flux through the coil core, the external magnetomotive force of the magnet drops by the amount of the increase in flux density through the internal reluctance of the magnet. (In the described magneto it drops from about 850 ampere turns per magnet to about 625 as the flux density in the magnet increases to about 11,500 lines per square inch.)

By estimating the minimum and operating flux densities in the magnet, the required magnetomotive force per unit length in the direction of magnetization can be obtained from the hysteresis curve for the magnet material (see Figure 3). The required length of magnet is now determined by the required external magnetomotive force of the magnet.

It can be seen from the preceding discussion that the magnetomotive force available at the magnet depends on the increase in flux density from minimum to operating flux density. The smaller this increase in density, the greater the magnetomotive force per unit length of magnet. This increase in density is minimized by using a large magnet cross section, as a certain increase in total flux gives a smaller increase in flux density. Any part of the leakage flux of the magnet which can be kept with the magnet at all times will be beneficial, as this will reduce the difference between minimum and operating flux density.

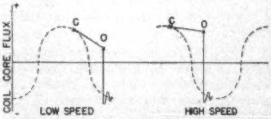

Figure 8. Plot of coil-core flux with magneto shaft rotation, showing variation in flux change from low speed to high speed

C—Breaker points close
O—Breaker points open

There are several modifying factors that so far have been neglected for the sake of simplicity. The first is "overlap," a condition which occurs in magnetos producing over about 200 sparks per second. If a magneto is capable of charging the harness to 12,000 volts, and the spark plug breaks down at 4,000 volts, most of the energy is still in the magnetic circuit, and it is dissipated as current flowing through the resistance of the coil and the distributor and spark-plug gaps (see Figure 4). The time constant of this circuit is such that under these conditions there is often an appreciable current flowing in this circuit when the breaker closes in preparation for the next spark. When the breaker closes, the time constant of the low-voltage winding being longer than that of the high-voltage winding, the current is transferred to the low-voltage winding. This current is in the opposite direction to the current which must flow in preparation for the next spark, and so it reduces the energy available for the next spark. Consequently, if the gap is rather large on the next spark plug to fire, it may miss. This condition can exist easily if there is a lead or carbon deposit which decreases the breakdown voltage of one spark plug.

The remedy for this condition is to make the time for decrement of the current as long as possible and to make the time constant of the circuit as short as possible. The former can be accomplished by closing the breaker as late as possible in the cycle, and the latter is accomplished in some magnetos by using a large capacitor in series with the high-voltage winding (see Figure 5).

A second design factor is compensation, which has already been mentioned in connection with the cam and breaker design. In a compensated magneto the sparks occur at a varying point in the magnetic cycle. To minimize overlap, the interval during which the breaker is open is constant in the described design. This means that the point of closing varies on one spark, and the point of opening varies on another, so that the interval during which the contacts are closed is quite variable, both in length and timing to the magnetic wave. For this reason, the flux change available for some of the sparks is much less than for others (see Figure 6). Since the magneto must be designed to provide sufficient energy on the weakest spark, there should be as little variation from spark to spark as possible. This can be accomplished by making the flux wave as nearly rectangular as possible (see Figure 7). In actual practice a rectangular flux wave can only be approached. By making the width of the teeth on the magnet cells as narrow as possible and keeping air-gap leakage as small as possible, a nearly flat-topped wave can be obtained. Since narrowing of these teeth increases the tooth flux density and therefore decreases the flux through the coil core because of saturation, a balance between these two factors must be obtained.

The third factor is design for sufficient voltage at low speeds. It has been stated earlier that the total flux change when the breaker opens is twice the flux in the coil core when the breaker is closed. This is only approximately true, since energy lost due to current flowing through the resistance of the low-voltage winding decreases the amount of the flux change when the breaker opens. Another way to show this is by a plot of the actual flux in the coil core with the breaker operating, at high and low magneto speeds (see Figure 8).

To increase the magneto output at low speed, therefore, the time constant of the low-voltage winding should be made long, and the time during which current flows should be made short. The former

A 120-Volt D-C Aircraft Electric System

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Synopsis: The practicability of using 120-volt d-c power in large airplanes has been a subject of discussion for some time. This paper describes the 120-volt d-c system and the equipment used on the Navy flying boat Mars, the service requirements, and the problems incidental to development and design of the components of the system. Calculated weights for several arrangements of generating equipment including the installed system are tabulated for comparison and the weight of the installed 120-volt d-c system is compared with the weight of the 28.6-volt d-c system. A summary report on flight tests and service operations provides the basis for conclusions relative to the practicability of 120-volt direct current for large aircraft.

FROM the inception of electric equipment in airplanes to the time of our entry into World War II the evolution of generating and utilization equipment followed a natural developmental trend. The first systems were built around a six-volt d-c supply and, as the requirements and physical size of airplanes increased, 12-volt systems* and 24-volt systems** made their appearances in order.[1] During this time the advantages of using still higher direct voltages and single-phase or three-phase alternating voltages were generally accepted, but development in this direction temporarily was retarded upon our entry into the war. Notwithstanding the limitations imposed on development and manufacturing personnel and facilities by the war, some progress has been shown in the application of high voltage to aircraft use, and some installations have been made and tested.

Attention has been directed primarily toward single-phase and three-phase a-c systems of 800 and 400 cycles, respectively, because of their advantages over high voltage direct current[2,3,4] under certain conditions but some interest in 120-volt d-c systems has been shown.[5,6] To the writer's knowledge only one installation of the latter-type system has been made and then flown for a sufficient time to make a preliminary appraisal of its practicability, that on the Navy flying boat Mars.

Historical Background

The first electric-load studies made on the preliminary design of the Mars indicated that some voltage higher than the 12–15-volt systems then in common use would be advantageous from a weight standpoint for the relatively-large loads contemplated. Studies were made early in 1938 on two electric supply systems based on proposed loads for the long-range bomber. Because most equipment at that time was designed for operation on 12–15 volts direct current, it was considered desirable to use higher voltages only on equipment which could be designed or converted easily and quickly. The first system consisted of a 24–30-volt d-c supply for motor and heater loads and 12–15-volt d-c supply for lights, instruments, signals, and other miscellaneous equipment. The second system differed from the first only by the substitution of 120-volt 400-cycle three-phase alternating current for 24–30-volt direct current for the motor and heater loads. Because it was assumed that the radio transmitter and receiver loads would be the same for both systems and would be supplied from a separate source of 120-volt 800-cycle, single-phase alternating current and 12–15-volt direct current, calculations did not include weights for any radio power supply. Generators in both systems were to be driven by auxiliary power plants. From these studies it was determined that the system using 24–30-volt d-c supply for motor and heater loads would weigh approximately 825 pounds more than that using 120-volt 400-cycle three-phase a-c supply. Further studies of weights and operating characteristics showed that 120-volt direct current for motor and heater loads possessed advantages over the proposed 120-volt 400-cycle three-phase alternating current, and in July 1939 a decision was made in favor of a complete dual-voltage d-c system comprising 120-volt and 30-volt d-c generators and 24-volt batteries. The change from 15-volt to 30-volt d-c generators was concurrent with a general movement in the aviation industry toward the higher voltage; this movement made available the necessary instruments, radio equipment, and so forth, designed to operate from a 28.5-volt bus. Provision for separate power supply for radio was eliminated.

Final decision on the 120-volt d-c system in preference to the 120-volt a-c system was based on several considerations:

1. The a-c system suffered a heavy weight penalty because of the necessity of supplying full electrical demand at altitude from auxiliary power plants. In contrast, the high-voltage d-c system provided a means of deriving most if not all of the electric power required in flight from the main engines, a procedure which at that time was not considered feasible in the case of the a-c system.
2. The torque–speed characteristics of d-c motors were known to be satisfactory for all applications involved, whereas little information was available on high-frequency a-c motors at that time and all such units would have required development.
3. Either main engines or auxiliary power plants could be used as prime movers, thus securing minimum weight and maximum operating economy under moored and flight conditions.
4. Any two or more operating generators could be paralleled at any time without recourse to elaborate and unproved synchronizing equipment.
5. Because of the relatively low operating altitude, commutation difficulties and reduced brush life would not be serious.[4,7]
6. The possibility of arc-rupturing problems would not become acute.[4]

This d-c dual-voltage system was developed and installed in the Mars, and it is the purpose of this paper to discuss the 120-volt portion of the system based on the actual installation and operating data relative to its applicability to this airplane.

Service Requirements

To obtain an idea of the electrical service requirements on the Mars, a short description of the airplane and its design characteristics will assist in clarifying the utility of the electric system.

The Mars is a four-engine Navy flying boat designed primarily as a long-range bomber but later converted to a long-

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*Consisting of 12-volt batteries and 15-volt generators, 14.25 volts at bus.

**Consisting of 24-volt batteries and 30-volt generators, 28.5 volts at bus.

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can be accomplished by using as much copper as possible in the low-voltage winding and keeping the resistance of the breaker plate and connections at a minimum. The latter can be accomplished by making the breaker closed interval short and having the flux wave as nearly rectangular as possible, as has already been discussed in connection with compensation.

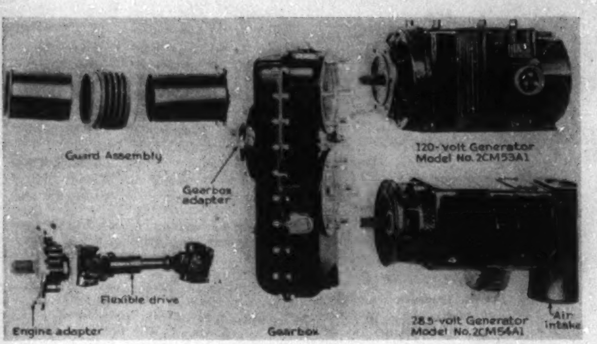


Figure 1. Subassemblies of speed-increasing gear box drive for main-engine 120-volt d-c and 28.5-volt d-c generators

Gear ratio 2.7:1, generator-speed range 5,600 to 11,200 rpm

range cargo transport of 140,000 pounds gross weight, 200 feet wing span, and approximately 119 feet over-all length. The flying range may vary from about 2,500 to 5,000 miles, and the cruising speed from about 125 to 240 miles per hour, depending on loading, power, and flying altitude. The service ceiling is approximately 15,000 feet.

The items which are connected on the 120-volt system are listed in Table I; part of the equipment is used only during moored operation, part is used only during flight, and some of the items are used during both moored and flight operation.

Since most aircraft instruments, propeller controls, and other accessory equipment already had been designed to operate on 28.5 volts and had been proved in service, no attempt was made to convert any of these items for 120-volt operation.

Generators

Economical generation of power for moored and flight electric loads of the long-range bomber, with provisions for reliability and standby capacity, necessitated the installation of both main-engine and auxiliary-engine generators. Design ratings of main-engine generators were fixed by the maximum load requirements of the bomber version for flight, and allowance was made for approximately 25 per cent spare capacity, or, under emergency conditions, by one main-engine generator and one auxiliary-engine generator with approximately ten per cent spare capacity.

Thus the total 120-volt generating capacity of the present airplane, based on the bomber requirements, comprises the potential output of four specially designed generators, two of which are driven by main engines and two by Lawrance auxiliary internal-combustion engines.

The removal of several relatively large turret motors and the addition of the flight-controls boost hydraulic pumps during conversion to the present type of airplane resulted in a net decrease in the maximum load. Because of this and because the probability of generator failure is less on this type of airplane, generator capacity is more than adequate to carry the present loads when compared with the total capacity of systems used on other airplanes of this type.

MAIN-ENGINE GENERATORS

In the case of the main-engine generators, the space available for accessory mounting on the rear of the engines is limited, and the generator take-off mounting flange is limited to a maximum bending moment which precluded the direct installation of a single dual-voltage generator or two single-voltage generators without changing the engine design. In addition, it was necessary to employ a step-up gearbox because of the low ratio available at the accessory drive pads. To circumvent these conditions one 120-volt generator and one 30-volt generator were mounted on a speed-increasing gearbox remotely coupled to the generator drive pad on the rear of the engine by means of a flexible shaft, Figure 1 and Figure 2.

The 120-volt generators are square-frame four-pole fully compensated commutating-field units having the following ratings: 8.75 kw, continuous; 14.50 kw, five minutes; and 17.25 kw five seconds; for a generator speed range between 5,600 and 11,200 rpm, and a reduced continuous rating of 6.25 kw at 4,200 rpm. These generator speeds correspond to 60, 120, and 45 per cent of rated engine speed, respectively. However, operation over the entire design speed range is not being realized because of voltage-regulator limitations, and at present the design ratings are obtained for a speed range between 7,200 and 11,200 rpm with a reduced continuous rating of 6.25 kw at 5,760 rpm.

These units were specially designed by the General Electric Company for this application, and their ratings are based on cooling by an air-blast pressure of three inches of water. The fact that the generators were designed to operate at this low pressure (which pressure was initially considered to be the maximum obtainable for the design speed of the airplane) and over a wide engine-speed range is responsible for the large weight per kilowatt. It has since been found that a ram pressure of six inches of water is available and that the original wide design-speed range is not required. Both of these factors would contribute to a

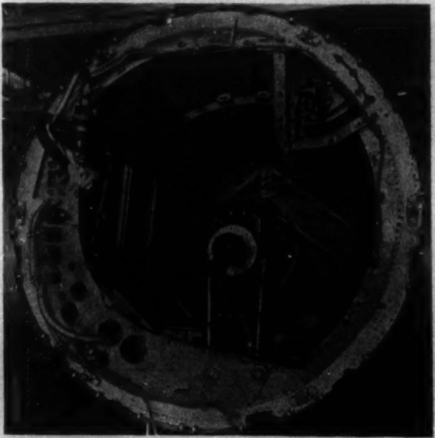


Figure 2. Speed-increasing gearbox, generators, and flexible drive shaft in position in nacelle before installation of engine

reduction of generator weight if redesign were contemplated.

Two remote-driven main-engine generator units as already described are used, one on engine 2 and the other on engine 4.

AUXILIARY-ENGINE GENERATORS

Each auxiliary-engine generating unit consists of dual field structures in a single frame and dual armatures on a common shaft, and is constructed to function electrically as two independent generators at 120 and 30 volts with separate voltage regulators and reverse-current relays, Figure 3. The two armatures are driven simultaneously by a vertical-shaft five-cylinder Lawrance internal-combustion engine.

These dual-voltage units were specially designed for this installation by the General Electric Company and the 120-volt sections are four-pole fully compensated commutating-pole type with ratings as follow: 6.75 kw continuous and 10.12 kw for five minutes at a generator speed of 4,000 rpm.

The 30-volt section also serves as a starting motor for the auxiliary engine, receiving energy from a 24-volt aircraft storage battery.

Two auxiliary-power-plant units as previously described are used for anchor operation and for stand-by capacity in flight and are installed in a fume-tight compartment in the hull of the airplane to facilitate servicing at all times.

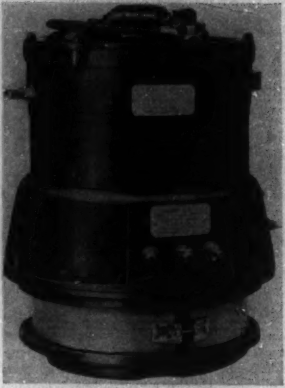
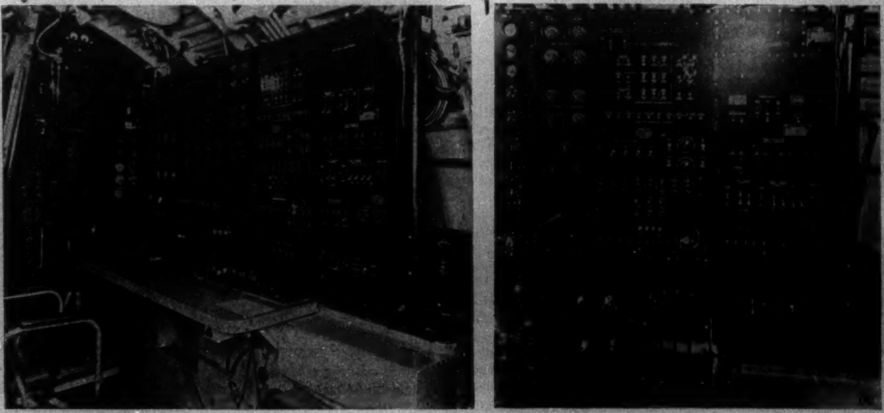


Figure 3. Auxiliary-power-plant dual-voltage 120–28.5-volt d-c generator for vertical-shaft operation at 4,000 rpm

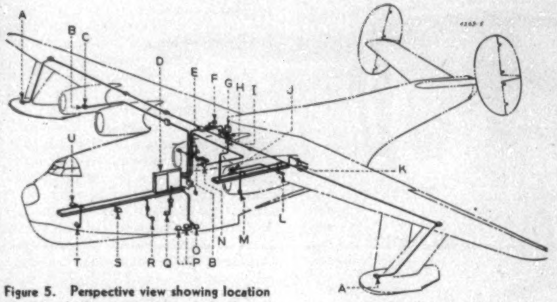
Regulation and Control Equipment

Complete control of the electric system is maintained by the flight engineer. Views of the flight engineer's panel are shown in Figures 4A and 4B; all 120-volt controls are mounted on the right-hand section, and all 28.6-volt controls in addition to ammeters and voltmeters for both systems are mounted on the adjacent section. The ammeter and control switch attached to the lower right-hand side of the 120-volt panel are for an additional 30-volt generator mounted on engine 3. The compartment below and in front of the 120-volt panel and a similar one directly behind contain all voltage regulators and reverse-current relays for the main-engine and auxiliary-engine generators. This arrangement facilitates adjustment during trial paralleling tests and also during periodic and emergency servicing. In addition to the 120-volt regulating equipment, these compartments also contain the 28.6-volt regulating equipment, and necessary ventilation is supplied by four centrifugal blowers directly connected to the 30-volt-generator terminals so that they are on at all times during generator operation and require no attention by the flight engineer. The 120-volt regulators, reverse-current relays, and all motor controllers are specially designed, but all circuit breakers and switches are standard stock items with the exception of the fuel-pump relays and the general hydraulic-system pressure-maintaining switch.



Figures 4A and 4B. Flight engineer's panel with 120-volt d-c controls on large right-hand section and 28.6-volt d-c controls on the adjacent section

Voltage regulators for the specially built 120-volt main-engine generators are adaptations of standard low-voltage General Electric finger-type units. Initially, each 120-volt unit consisted of three rows of contact fingers, totaling 30, which were actuated in sequence by the armature of a voltage-sensitive coil in order to vary the regulating resistance. These units, in initial tests, appeared to operate satisfactorily, but after the first flights routine inspections frequently revealed that contact fingers were burned off, although no reports of unsatisfactory operation were ever made. The facts that the regulators were used on high voltage, that the generator capacity was exceptionally high (17.25 kw for five seconds), and that the speed range was large (2.6 to 1) were all considered, but it was finally concluded that improper sequence of operation of the fingers was the cause of their burning off. It should be noted that the finger contacts were handling approximately maximum safe wattage and that any deviation from the proper sequence caused the fingers to insert two or three times as much resist-


A
B
C
D
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G
H
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U
A

Figure 5. Perspective view showing location of 120-volt d-c equipment and distribution cables in the Mars

A—Wing-float receptacle
B—To 120-volt generator
C—Nacelle junction box 4
D—Flight engineer's control panel
E—Cargo-hoist motor
F—General hydraulic-pump motor
G—Distribution box, auxiliary-power-plant compartment
H—Hydraulic-control booster pump motors (2)
I—To auxiliary-power-plant generators
J—Grill receptacles (3)
K—Aft ventilation motor
L—Galley receptacles (2)
M—Refrigerator receptacle
N—Nacelle junction box 2
O—Condensate pump motor
P—Fuel-pump motors (2)
Q—Bilge-pump motor
R—Receptacles, officer's stateroom
S—Vacuum-pump motors (2)
T—Forward-vent motor
U—Anchor-winch motor

ance as they were designed to handle which resulted in sustained arcs and consequent burning of contact fingers. To provide more positive sequencing of fingers, thus eliminating the effects of vibration, accumulations of dust, and so forth, would require a greater total motion of the finger-lifting bar, which was not obtainable without special development of a stronger moving system. The urgency of the units precluded the possibility of special development, and it was decided to revert to a unit with the normal number of 24 fingers in order to obtain more lift between fingers. However, to adapt these units to this application it was necessary to control the same amount of resistance with 80 per cent of the former number of steps, and, since no substantial increase in resistance per step at the high-current end of the stack was possible, it was necessary to make very large increases in resistance per step at the low-current end. These large resistance steps on the low-current end of the stack caused relatively large voltage changes, approximately 1 to $1^1/_2$ volts, when the generators were operating near the upper end of the speed range. For single-generator operation this degree of regulation would not have been overly objectionable, but during parallel operation this rough regulation caused the reverse-current relays to chatter almost continuously, especially when the total load was small.

As a result of power-plant and other developmental changes of the airplane up to the time of this trouble, the lower limit of normal engine-speed range was raised appreciably, thus eliminating the necessity for regulating voltage over the exceptionally large generator-speed range. The reduction in required regulating range alleviated the difficulty, since it permitted the interposition of a fixed resistor in the field circuit which limited the maximum field current to approximately 60 per cent of its former value. It also allowed increases in resistance per step on the high-current end of the stack to approximately 250 per cent of their former value without exceeding the maximum wattage per finger contact These larger resistance steps at the high-current end of the stack completely eliminated the necessity for the objectionable large steps at the low-current end. With the changes described, the 24-finger main-engine-generator regulators now function smoothly over the reduced speed range with generators operating either singly or in parallel. Ratings for the reduced speed range are shown in Table I for the present main-engine generator–regulator combination. Similarly designed regulators for the constant-speed auxiliary-power-plant generators operate satisfactorily.

The starter for the flight-controls boost-hydraulic-pump motor received special attention because under heavy loads and with low temperature it was thought that the motor might not come up to speed in a reasonable time and that this would be injurious to the counter-electromotive-force starter. An assembly with the desired time-delay characteristics was made, using commercial stock parts, which stepped up the voltage applied to the motor after fixed intervals of time, regardless of motor speed. Later, the time-delay features were found unnecessary and a carbon-pile counter-electromotive-force starter of approximately one-fourth the weight was substituted, and this has served very satisfactorily.

Tests on the generating system showed erratic and unpredictable operation of the reverse-current relays, which were specially designed for this application. Investigation revealed that stray flux from the contactor coils of the four relays,

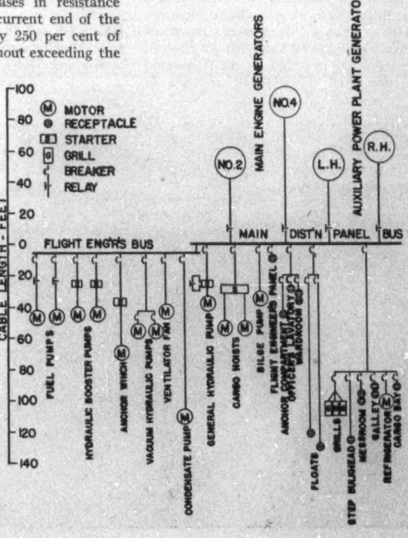
CABLE LENGTH - FEET
100
80
60
40
20
0
20
40
60
80
100
120
140
Ⓜ MOTOR
RECEPTACLE
STARTER
GRILL
BREAKER
RELAY
MAIN ENGINE GENERATORS
NO.2
NO.4
AUXILIARY POWER PLANT GENERATORS
L.H.
R.H.
MAIN
DIST'N
PANEL
BUS
FLIGHT ENGR'S BUS
FUEL PUMPS
HYDRAULIC BOOSTER PUMPS
ANCHOR WINCH
VACUUM HYDRAULIC PUMPS
VENTILATOR FAN
CONDENSATE PUMP
GENERAL HYDRAULIC PUMP
CARGO HOISTS
BILGE PUMP
FLIGHT ENGINEER'S PANEL
ANCHOR COMPARTMENT
OFFICER'S LAVATORY
WARDROOM
FLOATS
GRILLS
STEP BULKHEAD
MESSROOM
GALLEY
REFRIGERATOR
CARGO BAY

Figure 6. Single-wire schematic diagram of 120-volt d-c generation and distribution system showing the relative lengths of cables

Table I. 120-Volt D-C Equipment and System Load Conditions

Item	Amperes Per Unit	Number of Units	Units Operating Simultaneously	Seconds Per Operation	Operations Per Hr Per Unit	Moored			Flight		
						Normal 1 Hr	Preparation 1 Hr	Starting 30 Min	Take-Off 10 Min	Land 10 Min	Cruise 1 Hr
Motor cargo hoist	42.1	2	1	120	6		42.1				
Motor anchor winch	13.5	1	1	300	1			13.5			
Motor fuel pump	4.0	2	2	C	C			8.0	8.0	8.0	8.0
Motor general hydraulic pump	48.0	1	1	30	*		*48.0		*48.0	*48.0	
Motor ventilation blower	6.0	2	1 or 2	C	C	6.0	6.0	6.0	6.0	6.0	12.0
Motor hydraulic-pump auto pilot	7.0	2	1	C	C						7.0
Motor bilge pump, fixed	3.0	1	1	C	C	3.0	3.0	3.0	3.0		
Motor bilge pump, portable	3.0	1	1	10 min per hr		3.0	3.0	3.0			
Motor heater controls and condensate pump	7.4	1	1	C	C	7.4	7.4	7.4	7.4	7.4	7.4
Motor flight-controls boost hydraulic pump	16.7	2	1 or 2	C	C			33.4	33.4	33.4	16.7
Motor refrigerator	2.3	1	1	600	3	2.3	2.3	2.3	2.3	2.3	2.3
Grill	9.1	3	1 to 3	C	C	27.3	27.3				27.3
Percolator	4.1	2	1 or 2	C	C	8.2	8.2				8.2
Maximum demand, amperes						57.2	147.3	43.2	108.1	105.1	88.9
Short-time peak (rms first 5 minutes), amperes						57.5	147.3	43.2			
Rms balance of period, amperes						53.2	74.0	26.0			
Short-time peak (rms first 2 minutes), amperes									87.4	84.6	88.9
Rms balance of period, amperes									60.1	57.1	87.7
Total connected load, amperes	274.5										
Total connected generating capacity, amperes	258.0										

C—Continuous.

* Preparation—14 operations; take-off—2 operations; land—2 operations.

Generator Ratings:

Main-engine 120-volt d-c generator
7,200 to 11,200 rpm corresponding to 75 per cent to 117 per cent rated engine rpm
Continuous 8.75 kw = 73 amperes
Five minutes 14.50 kw = 121 amperes
Five seconds 17.25 kw = 144 amperes
5,760 rpm corresponding to 60 per cent rated engine rpm
Continuous 6.25 kw = 52 amperes
Five seconds 7.50 kw = 62 amperes
Auxiliary-engine 120-volt d-c generator 4,000 rpm
Continuous 6.75 kw = 56 amperes
Five minutes 10.12 kw = 84 amperes

which were assembled on a common panel, caused any one relay contactor to influence the pickup and dropout voltages of adjacent relays. The character of influence was dependent on their relative location. Although magnetic shielding was rather ineffective, more satisfactory operation was obtained by lowering the pickup voltage of the pilot contacts.

It was found necessary to provide ram-air-pressure cooling to the flight-controls boost hydraulic motors in order to prevent overheating under normal operation. Overheating was attributed to the location of the motors, since it was imperative to provide completely enclosed explosion-proof units. In this connection future designs will locate motors in spaces free from fumes, thus reducing weight by eliminating cooling accessories.

The design of the fuel-pump relays and the hydraulic-pressure switch only involved functional features adaptable to the specific applications.

Distribution and Feeder System

The 120-volt distribution and feeder system was designed as a two-wire ungrounded system in conformance with Navy specifications, supposedly as a precaution against injury to operating and servicing personnel,[8] while the 28.6-volt system followed the conventional single-wire ground-return design.

The disposition of loads and the approximate relative lengths of feeders for the 120-volt system are shown in Figures 5 and 6. All cables are completely protected and, wherever practicable, 120-volt cables and 28.6-volt cables are run in common conduits and ducts.

Utilization Equipment

As noted in Table I, fixed equipment consists of 15 motors and three grills with portable equipment consisting of one motor and two percolators. Of these motors, the cargo hoist, general hydraulic pump, anchor winch, and surface-controls boost hydraulic pump are large enough to require starting devices, all of the counter electromotive-force type, the first three being step-resistance units and the last a carbon-pile unit using much the same basic construction as the now familiar carbon-pile voltage regulators used in conjunction with standard 30-volt generators. Operating coils for all relays and contactors in the 120-volt system are energized from 28.6-volt control circuits.

All motors and other 120-volt power-consuming equipment are of special design for aircraft application with the exception of grills and percolators which are standard stock items.

Radio Interference

Responsibility for the radio installation and its operation was assumed by the Navy, and the urgency of aerodynamic and operational flight-test data interfered with routine measurements correlating electric-equipment wiring and operation with radio performance. The presence of numerous temporary flight-test-measurement circuits in the airplane and normal high ambient interference level prevented anything but a superficial ground check on main-engine and auxiliary-power-plant ignition and power-circuit influence voltages. This check revealed defective installations of the main-engine and auxiliary-engine ignition systems which were satisfactorily corrected, but indicated almost negligible influence voltages from the 120-volt and 28.6-volt power systems. Detailed reports on radio performance have not been received since the airplane was put into regular service, but informal statements indicate no unusual difficulties.

Operating Conditions

With the possible exception of periods near the end of an unusually long flight, the main-engine speeds will vary from 2,800 rpm at take-off to approximately 1,800 rpm during normal cruising. Near the completion of long flights the lower limit of engine speed may be reduced to approximately 1,600 rpm, a speed dictated by resonance characteristics of engines and mechanical structure and not by aerodynamic considerations. Thus, during normal flight, with an overall step-up gear ratio of 4.01:1, the main-engine-generator speed range is between 7,200 and 11,200 rpm and at the end of a long flight the minimum speed may drop to about 6,400 rpm. With the installed main-engine generators and voltage regulators rated power output obtains under normal flight conditions, but operation at

6,400 rpm results in reduced output as noted on Table I.

During take-off and landing it is common practice to operate either one or both auxiliary-power-plant generators in parallel with the main-engine generators, thus providing standby capacity in the event of a main-engine-generator failure, but during cruise auxiliary power plants are normally inoperative.

Each generator voltage is regulated at the generator terminals of its respective reverse-current relay. Load division between paralleled generators obtains as a function of voltage-drop differentials between paralleling resistors which are connected across the generator compensating and commutating fields. The taps on the paralleling resistors are adjusted so that, with two or more generators paralleled, the total load is divided approximately in proportion to the ratings of the operating machines.

Maximum demand, short-time peak, and rms value for balance of period, as shown on Table I, are pessimistic values, since under the conditions shown values are based on the assumption that all items which are to be used under a given operating condition function simultaneously at full rating. In general, this severe condition will occur infrequently. Inrush currents caused by the usual sequence of motor starting for the operating conditions listed are within the short-time rating of the generators normally operating; however, short-time capacity is not provided for simultaneous starting of all equipment as might occur under certain emergency conditions.

Table II. Power Requirements for Weight Studies

A. Maximum Power Requirements Used in Weight Studies

	Start	Run	Average	Peak*	Maximum	Average
	120-Volt D-C			28.6-Volt D-C		
Moored load	27.04 kw	14.63 kw	8.14 kw	9.29 kw	5.10 kw	3.52 kw
Flight load	51.91 kw	21.48 kw	10.95 kw	9.21 kw	7.25 kw	5.15 kw
	120-Volt 3-Phase 400-Cycle A-C			28.6-Volt D-C		
Flight load	32.7 kw 73.5 kva	20.3 kw 28.0 kva	10.8 kw 15.1 kva		9.63 kw	6.50 kw

* It was assumed that the batteries would carry the peak load over and above the maximum load; thus peak load was not considered when determining the 28.6-volt d-c generator ratings.

B. Calculated Power Requirements of Bomber Version

	120-Volt D-C		28.6-Volt D-C	
	Maximum Demand	Rms	Maximum Demand	Rms
Moored load	18.4 kw	10.6 kw	7.66 kw	3.18 kw
Flight load	24.8 kw	14.0 kw	11.21 kw	6.16 kw

C. Actual Power Requirements of Installed System

	Maximum Demand	Short-Time Peak	Rms	Maximum Demand	Short-Time Peak	Rms
Moored load	17.7 kw	17.7 kw	8.9 kw	8.8 kw	6.1 kw	3.3 kw
Flight load	13.0 kw	10.7 kw	10.5 kw	15.4 kw	9.6 kw	6.9 kw

Weight Studies

In 1939 prior to the final selection of the all d-c system now in use, weight studies were made on several power-supply systems to determine the one considered most practicable for this application. Practicability was based almost entirely on weight and reliability with space receiving secondary consideration. (Since the electric system for this airplane was conceived to provide power for armament equipment, the reliability factor was of major importance.)

Weight studies were based on horsepower requirements for maximum flight and moored loads with compensation for generator and gear efficiencies and included weights of prime movers, gearing, generators, mountings, fuel and oil systems, cooling systems, wiring, ducting, and controls. Table III shows calculated weights of the several systems based on requirements as noted in Table II, each of which includes the total weights of the 120-volt and 28.6-volt systems up to the distribution bus but not including the 24-volt storage battery which would be the same in each case. System 3 has a decided weight advantage over all other d-c–d-c and a-c–d-c systems studied, with systems 4 and 2 following in order. It will be noted that systems 3 and 4 provide 33$^1/_3$ per cent standby flight-generated capacity plus the altitude output of the auxiliary-power-plant generators and no standby capacity under moored condition. In contrast to this, system 2 provides 100 per cent standby flight-generated capacity plus the altitude output of auxiliary-power-plant generators and 100 per cent standby under moored condition.

The reliability of systems 2, 3, and 4 when subjected to enemy gunfire may be summed up in a few words. One hundred per cent capacity would be available in any one of the systems after the loss of one main-engine generator; yet the probability of the loss of a single generator is approximately twice as great for systems 3 and 4 as it is for system 2. The loss of two main-engine generators in any one of the systems would necessitate operation of at least one of the auxiliary-power-plant generators in any case; yet the possibility of the loss of the second main-engine generator is approximately three times as great for systems 3 and 4 as for system 2.

The system selected for installation was similar to system 2 of the weight study using two main-engine generators and two auxiliary-power-plant generators, but with normal moored and flight standby capacities of approximately 25 per cent. This system depends on flight operation of auxiliary power plants at reduced rating for additional emergency standby.

Weights of distribution and feeder cables and utilization equipment and controls were not included in these studies, since the locations of equipment had not been fixed. Also, because the weight of utilization equipment and length of feeder cables varies with the size of the airplane and its purpose, and because of the dissimilar nature of loads carried on the high- and low-voltage systems, weight figures on the distribution system external to the main distribution bus are of little value for general design purposes.

The dual-voltage installation on the Mars lends itself very well to a study of weights of the generating systems because of the similarity of location of the component elements.

From Table IV, pounds per kilowatt for the 28.6-volt system and the 120-volt system are calculated to be 65.6 and 39.0, respectively. The weight per kilowatt of the combined 28.6- and 120-volt system for the Mars is 45.5 pounds, and that for system 2 of Table III is 40.6 pounds; thus Table III appears to be within reasonable limits of accuracy.

Service Observations

The opportunity to make direct observations of the 120-volt system in flight has been limited to those flights made for performance test purposes, the total time of which was approximately 195 hours. This included a 32-hour nonstop endurance flight covering 4,600 miles, during which four floodlights totaling about five kilowatts were operated almost continuously in addition to the normal loads. During these tests the equipment was subjected to almost every condition of operation normally encountered in flight and some conditions which were more severe than those normally encountered.

Table III. Calculated Weights of Several System Combinations

Systems Studied	Generating Units: No.	Rating of Each	No.	Rating of Each	Total Weight of System (Pounds)
	120-Volt D-C		28.6-Volt D-C		
1*A*. Total moored or flight load supplied by either of two auxiliary power plants*	2	11.50 kw	2	5.50 kw	2,650
1*B*. Total moored or flight load supplied by any two of three auxiliary power plants*	3	5.75 kw	3	2.75 kw	2,566
2. Total moored load supplied by either of two auxiliary power plants.*	2	8.60 kw	2	3.70 kw	
Total flight load supplied by either of two main-engine generators**	2	11.50 kw	2	5.50 kw	2,382
3. Total moored load supplied by two auxiliary power plants*	2	4.30 kw	2	1.85 kw	
Total flight load supplied by any three of four main-engine generators**	4	3.85 kw	4	1.80 kw	1,872
4. Total moored load supplied by two auxiliary power plants*	2	4.30 kw	2	1.85 kw	
Total flight load supplied by any three of four exhaust turbogenerators #	4	3.85 kw	4	1.80 kw	2,352
	120-Volt 3-Phase 400-Cycle A-C##		28.6-Volt D-C		
5*A*. Total moored and flight loads supplied by either of two auxiliary power plants*	2	15.9 kva 11.4 kw	2	6.85 kw	2,493
5*B*. Total moored or flight loads supplied by any two of three auxiliary power plants*	3	7.5 kva 5.7 kw	3	3.43 kw	2,465
	120-Volt D-C		28.6-Volt D-C		
6. System selected for installation					
Total moored load supplied by two auxiliary power plants*	2	6.75 kw	2	2.00 kw	
Total flight load supplied by two main-engine generators**	2	8.75 kw	2	3.00 kw	1,866†

* Each auxiliary power plant driving one 120-volt generator and one 28.6-volt generator. Auxiliary power plants for flight operation were considered to give rated output at 22,000 feet.

** Each main engine driving one 120-volt generator and one 28.6-volt generator.

Each turbogenerator consisting of one 120-volt generator and one 28.6-volt generator.

The use of alternating current necessitated redivision of loads between high- and low-voltage systems.

† To facilitate comparison this weight does not include a 28.5-volt *P*-2 generator, installed on engine 3 and its accessories.

Table IV. Weight Comparison of Installed 120- and 28.6-Volt D-C Generating Systems Up to the Distribution Bus

Items	Pounds: 120 Volt D-C	28.6 Volt D-C
Main-engine generators	160.00	65.00
Flexible shafts, supports, gearboxes, ducting	* 71.12	* 24.38
Auxiliary power plants, accessories and controls	*516.24	*152.96
Auxiliary-power-plant generators and mountings	250.30	101.30
Voltage regulators	9.50	8.40
Reverse-current relays	20.00	8.00
Cables and terminals, plugs	107.44	213.67
Conduits, fittings, and clamps	23.56	29.59
Junction boxes, pull boxes, pressure seals	36.40	36.40
Ammeters and shunts	4.16	4.16
Voltmeters and selector switches	0.74	0.59
Ventilator blowers	6.80	6.80
Switches and fuses	1.32	1.45
Resistors	2.10	3.60
Total weight of generating system	1,209.68	656.30
Total weight of 120-volt and 28.6-volt generating systems		1,865.98
Storage batteries		72.00
Junction boxes		1.60
Battery disconnect and contactor		3.86
Cables and terminals		28.43
Conduits and fittings		4.20
Ammeter and shunt		0.95
Switch		0.31
Total weight of batteries and associated items		111.35

* Weights proportional to kilowatt rating of respective generators.

Smooth- and rough-water taxiing and take-off; dives; climbs; operation at 16,000 feet altitude; and two-, three-, and four-engine performance with various power values were among the more severe conditions of operation. Performance of the 120-volt equipment was reported satisfactory under all conditions.

After delivery of the airplane to the Navy in November of 1943, one nonstop trial flight was made from Patuxent, Md., to Natal, Brazil, a distance of about 4,300 miles with a mail load of 13,000 pounds; the return trip was broken up into several hops. Assigned to the Naval Air Transport Service, the Mars has been engaged in flights between Alameda, Calif., and Pearl Harbor, Hawaii, a round-trip distance of approximately 4,800 miles. With the exception of a mechanical failure in the shaft of one of the auxiliary-power-plant generator units, improper repair of which resulted in damage to the 120-volt field of that unit, the 120-volt system has functioned in a generally satisfactory manner. Neither of these failures can be attributed directly to failure of the 120-volt system to meet the service requirements but rather to mechanical design deficiencies which may be corrected with further operating experience. The voltage-regulator design has not proved completely foolproof, and occasional faulty operation is reported. However, unsatisfactory performance of these units often has been traced to careless treatment by personnel.

For comparison, it can be said that less trouble has been experienced with 120-volt equipment than with the conventional 28.5-volt equipment on this airplane.

Conclusions

Although most of the equipment used was specially designed for this installation, the fact that it was designed and built in 1939, 1940, and 1941 should not be overlooked. It is reasonable to believe that improvements could have been made on 120-volt d-c equipment comparable to those that have been made on 28.5-volt d-c equipment since that time with corresponding weight reductions and better operating characteristics. As an example, the main-engine generators are excessively large and heavy for the kilowatt rating, because initial calculations showed that at the design speed of the airplane the ram air pressure available for cooling at the generator intake would be limited to approximately three inches of water. It since has been found that up to six inches of water ram air pressure is available at the generators and that only under unusual conditions are the limits of the speed range approached.

Based on the conclusions of H. H. Viehmann,[8] "If absolute safety from accidental electrocution is thus to be avoided in aircraft, the supply voltages must be kept below 140 direct current and 35 alternating current respectively," the two-wire ungrounded system as employed in this installation is unnecessary from the standpoint of safety to personnel and the use of a single-wire ground-return system would result in reducing the conductor weight by almost one half.

In consideration of these factors and from observations made on the installation discussed, it is concluded that 120-volt d-c systems are practicable from weight, reliability, and safety standpoints for large commercial airplanes, providing the following conditions exist:

1. The high-voltage d-c system is supplemented by a low-voltage d-c system with connected storage battery to provide a source of power for engine starting, anchor lights, and boarding lights at times when the auxiliary engines and main engines are inoperative.

2. The airplane operates at low or medium altitudes.

3. A major portion of the electric load is composed of items such as motors and galley grills, which readily can be designed to utilize 120-volt power.

Continued development of 120-volt d-c equipment to expand the utility and reduce equipment weights will increase the advantages for applications of this type.

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Aircraft-Engine-Accessory Vibration

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THE vibration of aircraft power-plant accessories is one of those orphan problems which has been given "a thorough tossing around" for a good many years. For example, when an accessory manufacturer finds himself in the midst of an epidemic of accessory failures on a particular power-plant installation, he usually points to successful operation of the same accessory on other power-plant installations as proof of the fact that the failures are not due to a weakness in the accessory; the engine manufacturer points to other installations of the engine where the accessories are not in trouble; the airplane manufacturer easily proves that the airplane does not cause the failures.

Those familiar with aircraft power-plant vibration problems will recognize that the whole vibrating system must be considered in the analysis of the vibration of one of the members of the system. With the various parts of aircraft power-plant installations being built by several different manufacturers and designed to be interchangeable among other power-plant installations, it is not surprising that combinations of power-plant parts occasionally are put together which have unsatisfactory vibration characteristics. Since these vibration characteristics are not predictable, accessory failures are not the result of poor design. They are the result of bad luck.

In past years the only information available to guide the airplane manufacturer in his analysis of accessory vibration has been the Army–Navy specification which imposed a limit on the overhang moment.* Considerable progress has been made recently in the analysis of the problem by both analytical and experimental methods. This paper is presented to make available to the aircraft industry as a whole and to the accessory manufacturers in particular the latest information on aircraft power-plant accessory vibration. In September 1942, Installation Bulletin 15, "Accessory Vibration," was issued by Pratt and Whitney Aircraft to present data which had been accumulated on this subject up to that time.

A philosophy of design also is presented which has been in vogue at Pratt and Whitney Aircraft for several years and is believed to be fairly well accepted by a major part of the aircraft industry at the present time.

The Vibration System

Starters, generators, and cabin superchargers will be considered in this discussion. Hydraulic pumps so far have not become large enough to require consideration from the accessory-vibration standpoint. Tachometers mounted on the engine by means of adapters have had vibration trouble, but solution was simple —eliminate the adapter. From the power-plant vibration standpoint, starters, generators, and cabin superchargers are so much alike that we need not differentiate among them. They are all small relative to the engine, and all have natural frequencies in bending with respect to the engine in the same frequency range.

The vibration system to be considered is shown in Figure 1. It will be noted that only bending deflections are being considered. The engine is the largest mass, and the propeller is the next largest. The propeller shaft and the propeller blades are the most important springs in the system. The vibration of the engine–propeller system is usually independent of the accessories on the rear end of the engine. However, if a natural frequency of the accessory with respect to the engine happens to be close to a natural frequency of the engine–propeller system, the engine–propeller vibration will be modified appreciably by the presence of the accessory. Thus the accessory excitation, which is the engine–propeller vibration, may be studied without consideration of the accessories used, with reasonable accuracy with the exception already noted.

Engine–Propeller Vibration

The propeller selected for each airplane installation is usually the result of several compromises involving airplane performance, engine cooling, propeller weight, propeller-blade stress, airplane vibration, and so forth. The savings in propeller weight made possible by the reduction of engine–propeller vibration are occasionally on the order of 50 to 150 pounds per power plant. It so happens that for a given mode of vibration of the system, the propeller-blade stresses and stresses in the accessory attachments are approximately proportional to each other. For this reason the accessory manufacturer may rest assured that the basic cause of accessory vibration, the engine–propeller vibration, is being considered in the selection of the propeller and is being held to an absolute minimum.

The engine–propeller vibrations important in accessory-vibration studies are modes in which the power plant whirls about a node on the thrust line near the engine center of gravity at resonance, so

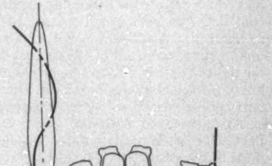

Figure 1. The vibration system

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* Overhang moment equals weight of accessory times the distance of the center of gravity of the accessory from the mounting face.

that an ellipse, usually a circle, is traced in the airplane of the accessory pads.

The engine–propeller-vibration spectrum, drawn on a scale of engine rpm, is a function of

(*a*). The vibration spectrum of the propeller mounted on the propeller shaft.

(*b*). The engine-crankshaft-to-propeller-shaft gear ratio.

(*c*). The orders of excitation produced by the engine.

The mass of the engine is so large compared to the propeller mass that variations in engine mass have no appreciable effect on this vibration spectrum.

Typical curves of the vibration of the engine at the center of the rear cover are shown in Figures 2 and 3.† Figure 2 is a plot of the data as recorded by the Pratt and Whitney Aircraft vibration analyzer showing the amplitude of each important order of vibration versus rpm of the engine. Figure 3 is a plot of the amplitude of vibration versus frequency, cross-plotted from Figure 2. It will be seen later that Figure 3 is more convenient for checking the accessory excitation produced by the power-plant installation against the response of the accessory installation.

The accessory manufacturers' field engineers should be interested in the curves shown in Figures 2 and 3 which represent the accessory excitation for a specific installation including a particular engine, propeller, and gear ratio. If the propeller or gear ratio is changed, the curves may be quite different. The accessory manufacturers' field engineers should have curves similar to Figures 2 and 3 for each of his accessory installations and should keep track of any change in propeller, gear ratio, or, as will be discussed later, other accessories on the same installation.**

The accessory engineers who are developing accessories to be used on a wide variety of engine–propeller combinations should use the type of curves shown in Figures 4 through 8 as an indication of the amount of excitation which the accessory may be called upon to withstand. The curves may be identified as follows: Figure 4—early *R*-1830 power-plant installations, Figure 5—revised *R*-1830 power-plant installations, Figure 6—*R*-2000 power-plant installations, and Figures 7 and 8—*R*-2800 power-plant installations.

The difference between the early and the revised models of *R*-1830 engines is that the latter incorporates modifications which are designed especially for the purpose of reducing $2^1/_2$-order propeller-blade stresses. The corresponding reduction in engine–propeller vibration, and so forth, was extremely helpful from the standpoint of accessory vibration. It might be concluded from this that, with further improvements in the engine along the same or similar lines, the amplitude of vibration of the engine could be expected to be reduced step by step. To date this has not been the case. As soon as a change is made to reduce propeller vibration, the propeller is lightened to take advantage of the reduced vibration stresses and the engine-vibration amplitudes and propeller-blade stresses come back again to the limiting propeller-blade-stress range. This is good design practice since it results in the lightest power-plant installation. However, for this reason we cannot hold out any hope to the accessory manufacturer for smaller amplitudes of engine vibration in this frequency range so long as present-type conventional engines and propellers are in use. On the other hand, it is unlikely that the amplitudes of engine–propeller vibration will be allowed to exceed those shown in Figure 4 where the propeller-blade stresses were just over the limit. Exceptions to this rule may be encountered if (*a*) the fatigue strength of propeller-blade material is increased or (*b*) the vibration characteristics of the blades changed to such an extent that new modes of engine–propeller vibration are introduced into the operating speed range. However, this is merely crystal gazing and should not be taken too seriously by engineers designing accessories to be mounted on power plants now in use or in the development stage.

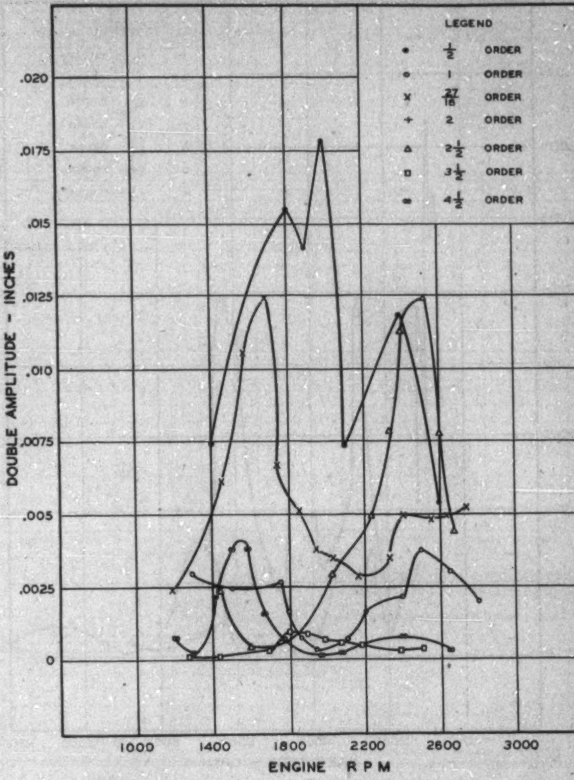

Figure 2. Typical vibration-frequency spectrum of *R*-1830 power-plant installation

Motion of the rear-cover face

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† The vibration curves are plotted as double amplitude (for no good reason except that we got started that way).

** Pratt and Whitney Aircraft has established the practice of presenting curves similar to Figure 2 in reports to airplane manufacturers covering vibration tests on power-plant installations, with a recommendation that copies of these curves be passed on to all accessory manufacturers who may be requested to supply accessories for the installation tested. This policy was established in September 1940 and has been followed ever since.

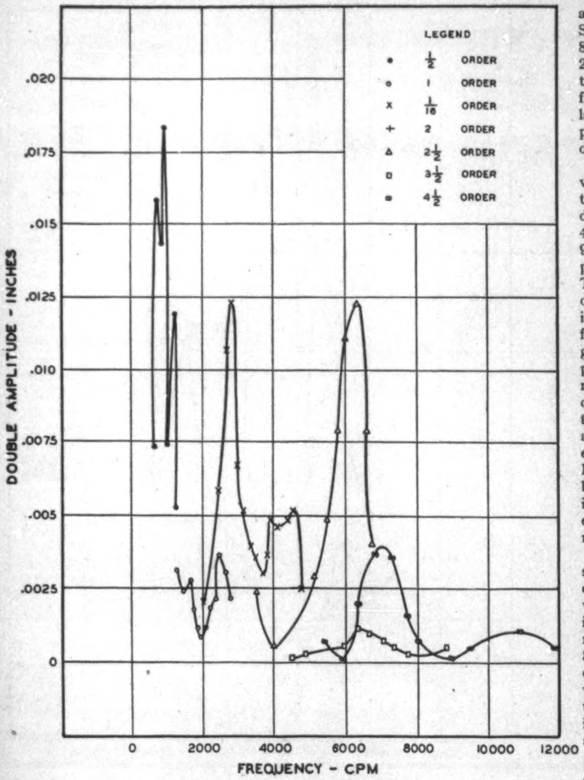

Figure 3. Typical vibration-frequency spectrum of *R*-1830 power-plant installation

Motion of the rear-cover face

In general the largest excitations for the 14- and 18-cylinder radial engines are $2^{1}/_{2}$ and $3^{1}/_{2}$ orders, respectively. The exciting frequencies of these orders for these engines at their maximum rated speeds are 6,750 and 9,450 cycles per minute, respectively.‡ In Figure 4 amplitudes over 0.020 inch are encountered in three installations. In one the curve peaks at 6,750 cycles per minute and in another at 6,250 cycles per minute. The difference between these two installations is that the latter has a slightly larger-diameter propeller than the former. If progressively larger propellers were used, this particular mode of vibration would occur at lower and lower frequencies. If progressively smaller propellers were used, this mode would occur at higher frequencies. In general, after an engine has been in service very long a variety of propellers will have been used on the engine, and enough spread will have been found in the engine–propeller natural frequencies practically to blanket the whole range of excitation produced by the engine.

Beyond 6,750 cycles per minute the maximum amplitude of vibration encountered in Figure 4 is that resulting from the $4^{1}/_{2}$-order excitation. The amplitudes of vibration of the engine at $4^{1}/_{2}$ order are appreciably lower than those at $2^{1}/_{2}$ order. Similarly, it will be noted in Figures 7 and 8 that the $3^{1}/_{2}$-order vibration of the *R*-2800 engine is the largest one and extends to 9,450 cycles per minute. Above this frequency the $5^{1}/_{2}$-order vibration is the largest encountered. The $5^{1}/_{2}$-order amplitudes are much lower than the $3^{1}/_{2}$-order amplitudes.

A chart, Figure 9, showing the engine vibration which may be encountered in the various frequency ranges, has been drawn from the data presented in Figures 4 through 8. The straight lines in Figure 9 are envelope curves drawn along the peaks of the engine-vibration curves. They are extended to the minimum frequencies at which the order of excitation is produced on the assumption that different propellers may be used on the engine which will raise or lower the engine–propeller natural frequencies into this range. A comparison of the vibration of individual installations in Figures 4 through 8 will show that the maximum amplitudes in many installations do not even approach these envelope curves. However, since these amplitudes have been encountered in several installations, it should be recognized that they may be encountered in any installation which has not been tested.

As already pointed out, there is considerable variation in the amplitudes of engine rear covers at resonance for the various installations tested. This is indicated by the difference between the maximum amplitudes on Figures 7 and 8. Considerable research will be required to determine the reasons for this variation in engine vibration. Probably the two most important factors contributing to this large variation in amplitude are:

1. Coupling between the various modes of vibration of the system.

2. Damping within the system and aerodynamic damping of the propeller.

One example of variation in coupling which introduces variation in engine–propeller vibration between installations is the change in propeller-blade angle with air speed. On a ground test stand the propeller operates with the blades in a relatively low blade-angle setting; whereas, in flight, particularly at high

Table I

Weight	45.0 lb
Distance of center of gravity from mounting flange	5.125 in.
Mass moment of inertia about a line through the center of the mounting surface	6.19 lb-in.-sec.^{2}
Natural Frequencies Obtained With These Accessories	
1. Dummy on rigid block	36,000 cpm
2. Dummy on *R*-2800 rear cover	14,500 cpm
3. Starter on rigid block	11,400 cpm
4. Starter on *R*-2800 rear cover	9,300 cpm

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‡ In the case of the *R*-1830 tests, data were taken up to engine speeds of 2,800 rpm although the highest rpm rating is 2,700 rpm. Engine speeds above the highest speed at which the engine has a rating sometimes are encountered in manuevers or are due to faulty operation of the propeller or governor. However, operation under these conditions usually is limited to relatively short periods of time. If the overspeeding is due to faulty controls, it will last just long enough to get the power plant back under control again. Overspeeding during manuevers usually lasts for a matter of three to five seconds only, and during this time the overspeeding may amount to 20 per cent.

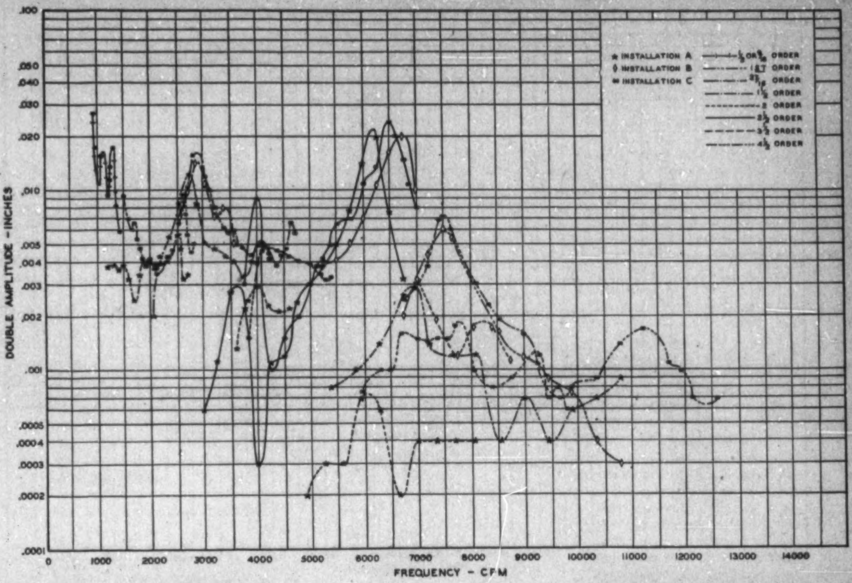
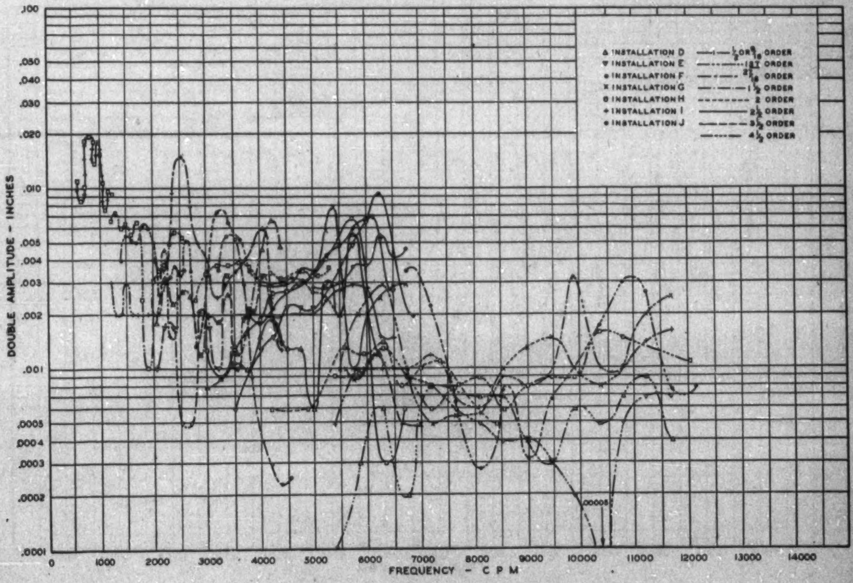


Figure 4 (above). Engine-vibration spectra of early *R*-1830 power-plant installations

Motion measured at rear of engine

Figure 5 (below). Engine-vibration spectra of revised *R*-1830 power-plant installations

Motion measured at rear of engine



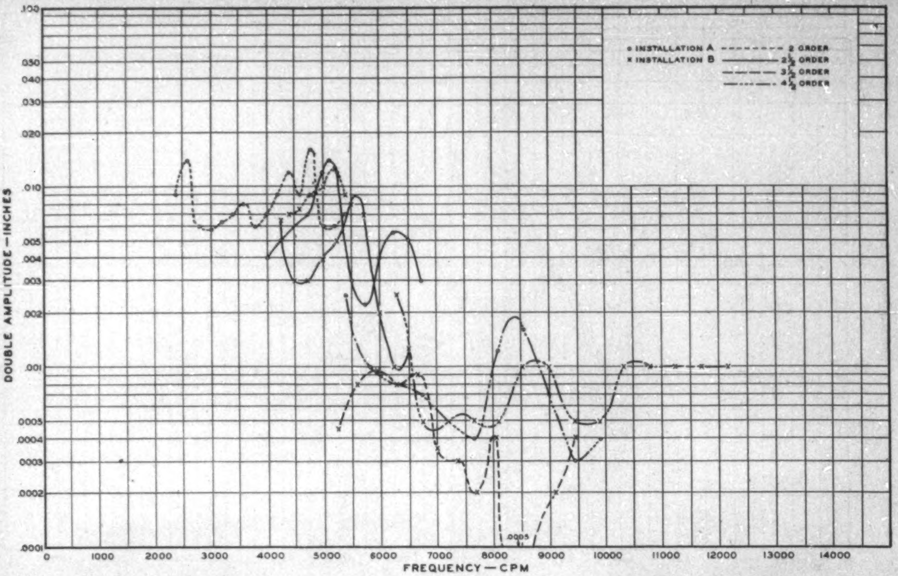
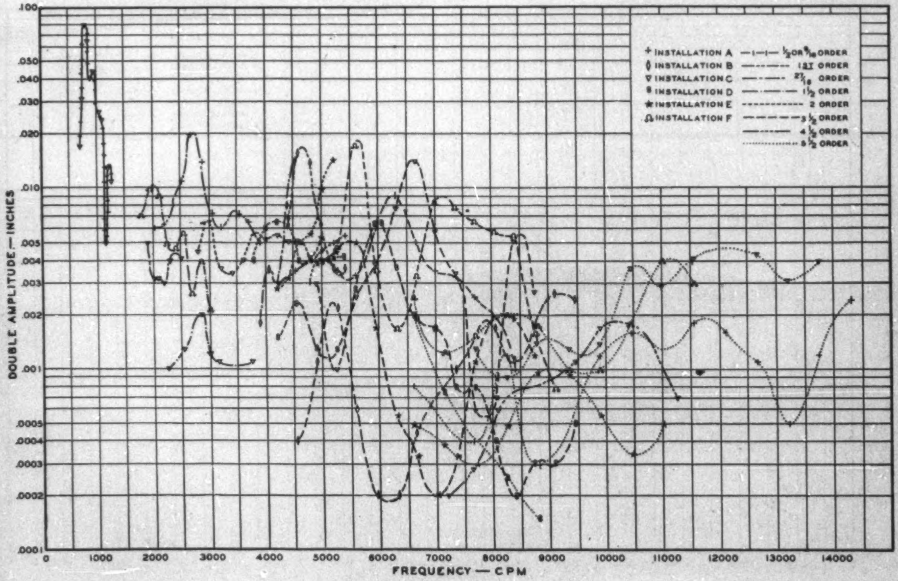


Figure 6 (above). Engine-vibration spectra of *R*-2000 power-plant installations

Motion measured at rear of engine

Figure 7 (below). Engine-vibration spectra of *R*-2800 power-plant installations

Motion measured at rear of engine, sheet 1



speeds, the blade angles are quite steep to accommodate the forward speed of the airplane. Changes in pitch produce changes in coupling between the bending vibration of the propeller blades and the whirling vibration of the engine and, therefore, changes in the response characteristics of the engine–propeller system. There is never enough information available to evaluate the magnitude of all of the various factors which produce changes in engine–propeller response characteristics, but tests show that these effects are important.

From the foregoing, it will be obvious to the layman that power-plant-vibration analysis is not an exact science. As a matter of fact, it is not at all unusual to encounter amplitudes of vibration twice as large or one half as large as those which had been predicted on the basis of tests run on other similar installations. For this reason a mass of data obtained on several different installations is probably the safest procedure to follow for accessory design and development purposes, even though some of the anomalies which will be found in the data cannot be scientifically explained.

Accessory Response Characteristics

Accessories do not ordinarily produce any vibration excitation themselves, but merely respond to the vibration of their support much the same as reed vibrometers respond to the vibration of the machine to which they are attached.

The response characteristics of an accessory are a function of:

(*a*). The mass of the accessory.

(*b*). The stiffness of the accessory, particularly the stiffness of the end of the accessory which is attached to the engine.

(*c*). The stiffness of the rear cover, particularly at the point where the accessory is attached.

(*d*). The other accessories mounted on the rear cover.

Figure 10 shows in (*a*) an elastic line for a single accessory–rear cover combination with respect to the engine, in (*b*) the same for two accessories, and in (*c*) the same for three accessories whose center lines lie in the same plane. Other elastic lines exist for two and three accessories at other frequencies and with other accessory arrangements. Starters and cabin superchargers are usually unsymmetrical, and it is possible to orient them in any one of several different positions. It is, therefore, obvious that systems frequently may be encountered which will not lend themselves to paper studies. In practice it has been found that the best way to determine the natural frequency of accessory–rear-cover combinations is by installing the accessory with the other accessories to be used on the rear cover and checking the natural frequencies experimentally.

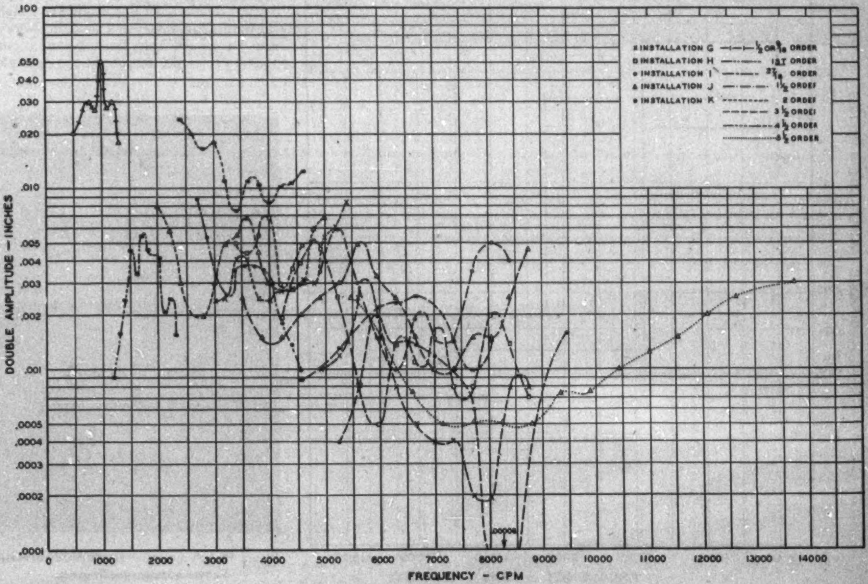
A set of data is presented in Table I to give some information regarding the stiffness of the rear cover to which the accessories are attached. To determine the ratio of the stiffness of a typical accessory to the stiffness contributed by the rear cover, a dummy starter as shown in Figure 11b, with a comparatively rigid attachment end but with the same dimensions and mass characteristics as the real starter shown in Figure 11a, was used. The natural frequency of a real starter when mounted rigidly on a large steel block was also determined. The mass and dimensional data on both starter and dummy are as given in the table.

These data indicate that the stiffness of the dummy starter was very high compared to the other stiffnesses involved and that the stiffness of this particular starter was about half of the rear-cover stiffness. Other accessory–rear-cover combinations obviously have other stiffness ratios.

This example indicates that 14,500 cycles per minute is about the upper limit of natural frequency for this starter mounted on an *R*-2800 rear cover. The dummy was constructed of steel tubing with the mounting flange extending in-

Figure 8. Engine-vibration spectra of *R*-2800 power-plant installations

Motion measured at rear of engine, sheet 2



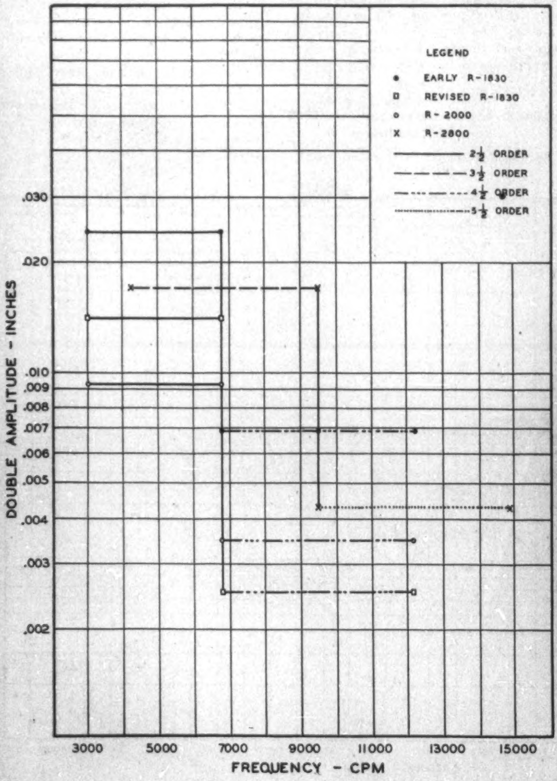
ward from the inside diameter, as shown in Figure 11b, instead of the usual construction in which it extends outward from the outside diameter of the necked-down end of the generator housing. This will serve to emphasize a fact that is now probably well appreciated by some accessory manufacturers, that a wall from the outside diameter of the mounting flange directly to the outside diameter of the housing is an extremely effective method of stiffening the accessory mounting.

In a practical installation there is usually more than one accessory on the rear cover of the engine. Typical installations for use as examples are selected which include:

1. A starter and a generator on the *R*-2800 rear cover.
2. A starter and two generators to simulate a starter, a generator, and a cabin supercharger.

When these accessories are mounted separately, their natural frequencies are for the starter only, 7,000 cycles per minute, and for each generator only, 6,250 cycles per minute. When the starter and one generator are mounted on the rear cover the lowest natural frequency is 5,520 cycles per minute. When the other generator is added to this combination, the lowest natural frequency is 4,850 cycles per minute. Higher natural frequencies are also present for other modes of vibration existing when more than one accessory is present. Thus, it is evident that the addition of a second and third accessory appreciably changes the vibration characteristics of the previous configuration. This is illustrated in Figure 10 which shows that the deflection of one accessory with respect to the rear cover will produce a deflection of the other accessories and vice versa; that is, coupling exists between the vibration of accessories. These examples illustrate the fact that the natural frequencies of present accessories may be encountered over a wide frequency range. This frequency range could be raised by stiffening the connection of the accessory to the rear cover. There is an upper limit to which the natural frequency of a single accessory can be raised, this limit being about 14,500 cycles per minute in the example

Figure 9. Envelope curves of maximum vibration at accessory mounting pads



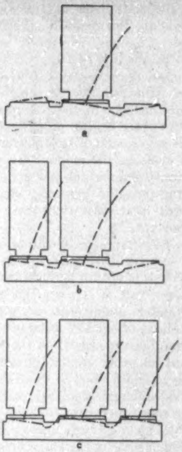


Figure 10. Elastic lines for accessory configurations

Figure 11. Accessories used in study of rear-cover accessory stiffnesses

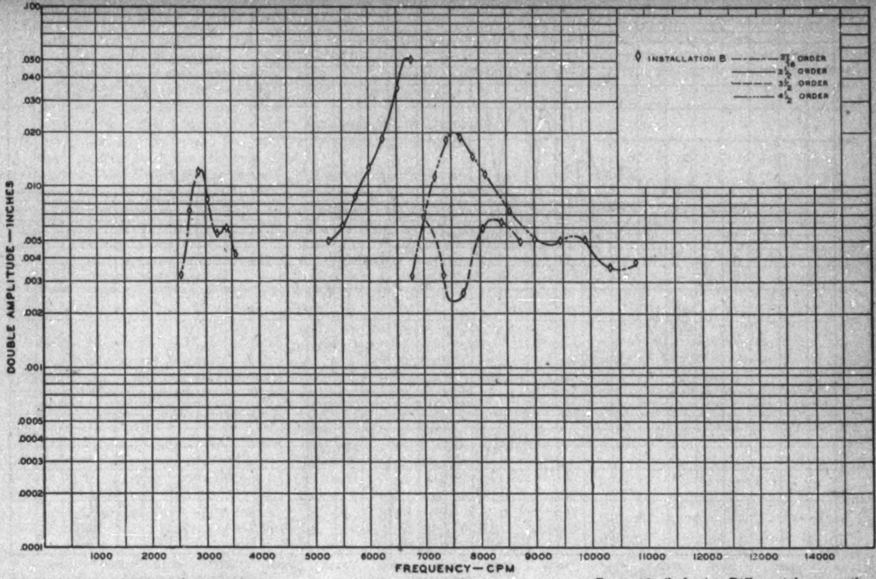
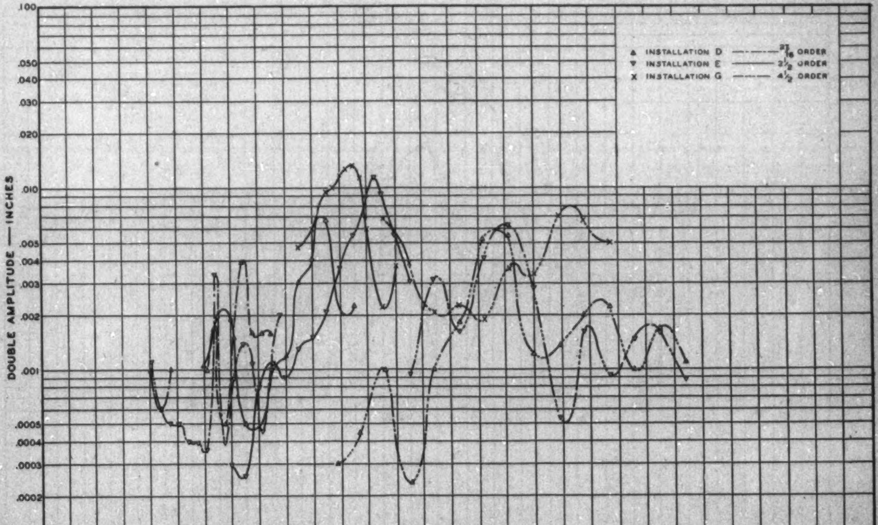


Figure 12 (above). Differential-starter-vibration spectra of early *R*-1830 power-plant installations

Measured at ten inches from mounting flange

Figure 13 (below). Differential-starter-vibration spectra of revised *R*-1830 power-plant installations

Measured at ten inches from mounting flange



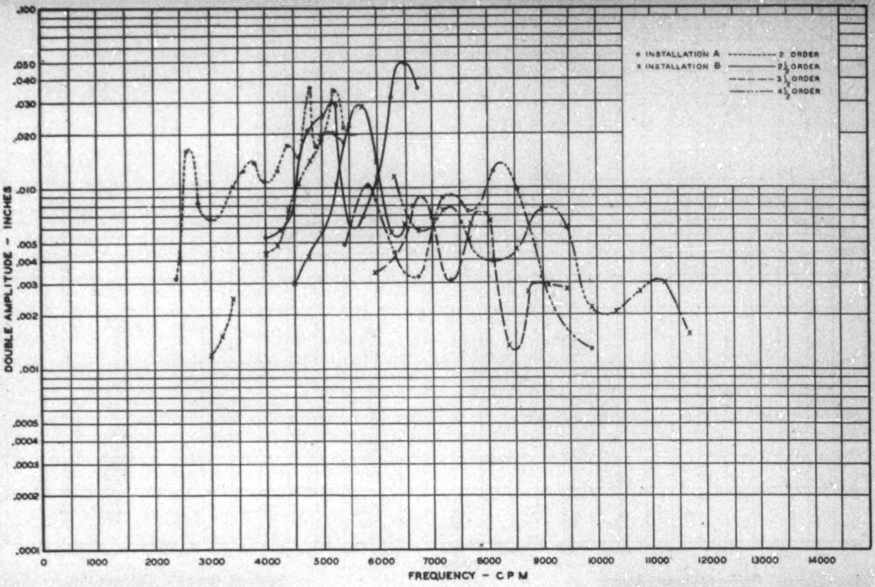
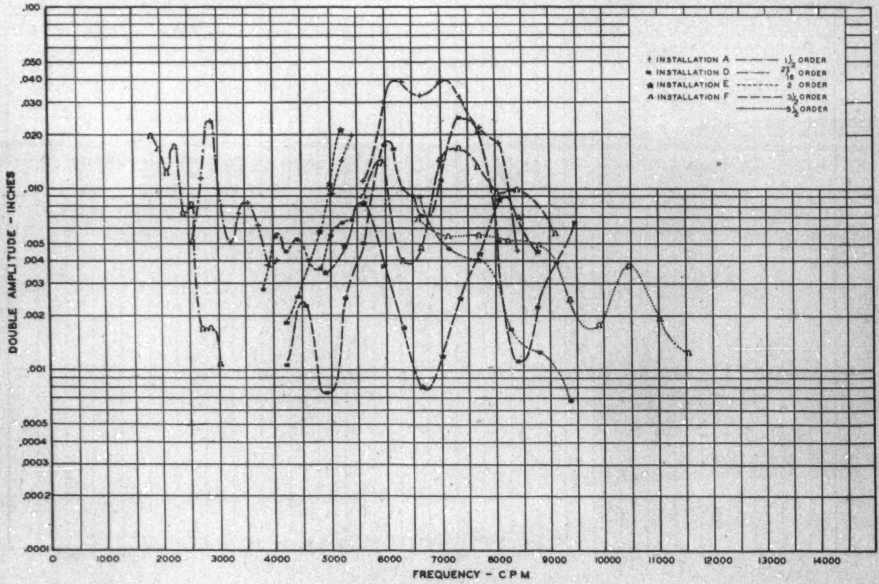


Figure 14 (above). Differential-starter-vibration spectra of *R*-2000 power-plant installations

Measured at ten inches from mounting flange

Figure 15 (below). Differential-starter-vibration spectra of *R*-2800 power-plant installation

Measured at ten inches from mounting flange



previously shown, but perhaps not over 10,000 cycles per minute in some cases. However, when two or three accessories are used, the first mode is certain to come at a frequency appreciably lower than this upper limit. This frequency often will be too low. In that case, the next best thing to do is to reduce the mass of the accessory, or at least reduce its moment of inertia about the mounting surface. Some progress is being made in this direction on starters which are being shortened in length and rearranged internally to bring the main starter masses closer to the mounting surface.

A series of curves in Figures 12 through 22 presents data on the vibration of starters, Figures 12, 13, 14, 15, and 16; generators, Figures 17, 18, 19, 20, and 21; and cabin superchargers, Figure 22. The installations listed in Figures 12 through 22 are the same installations as those listed in Figures 4 through 8, and the same symbols are used to facilitate cross reference. The accessory vibration is plotted as differential vibration,* that is, the vibration of the accessory with respect to the engine rear cover. Differential vibration is used, because it is a direct indication of the amount of bending of the accessory with respect to the engine. A determination of the relationship between accessory bending and stresses in the attachment parts may be desired, but to date it has been found most practical to measure differential vibration in flight and to make further studies in the laboratory if necessary.

Development of Accessories to Withstand Vibration

Testing accessories for endurance on aircraft engines is a rather lengthy, unreliable, and expensive method of proving the endurance characteristics of an accessory. It is obvious from the preceding discussion that an endurance test conducted on an engine–propeller combination selected indiscriminately gives results of little value in estimating the endurance of the accessory on any other engine–propeller combination. It would be obviously impractical to conduct an endurance test of an accessory on all possible engine–propeller combinations and with all other possible combinations of accessories on each engine. Furthermore new engines and accessories are being put into production every year, and it would be an endless task for an accessory manufacturer to continue checking all of his current and presumably completely developed models of accessories in combination with all other new accessories on each new engine. On the other hand, we have not reached the stage where it is possible to make final designs on paper. Endurance testing is vital, and, although some endurance testing on an engine is necessary, the bulk of this work should be accomplished by tests on an accessory-vibration-testing machine.

Accessory-Vibration-Testing Machine

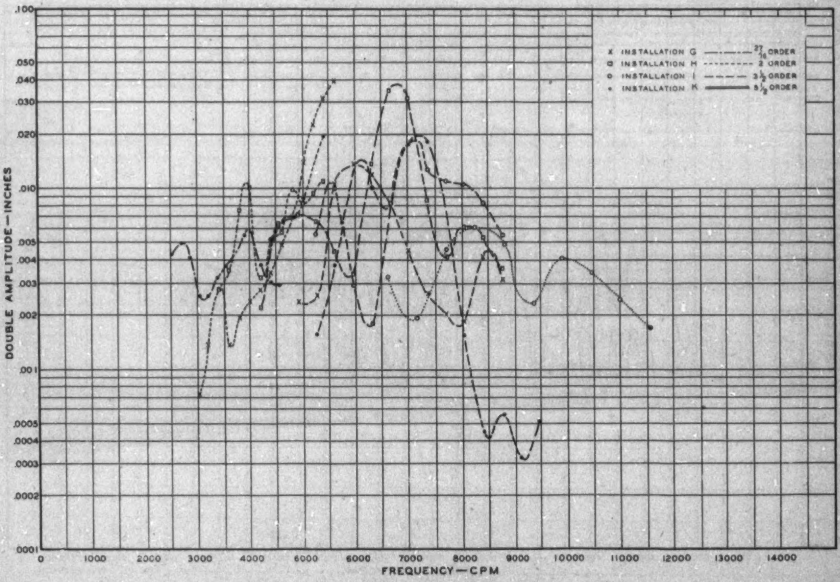
Several vibration-testing machines are in use at accessory manufacturers' plants. Pratt and Whitney Aircraft has supplied rear covers of *R*-1830 and *R*-2800 engines for attachment to these machines, so that the response characteristics of the accessories on actual engines could be duplicated as closely as possible. The three most important power-plant vibration conditions to be duplicated in an accessory-vibration-testing machine are:

1. The mode of vibration of the accessory installation on the engine.
2. The frequency range in which accessory natural frequencies will be encountered.
3. The excitation sufficient to produce amplitudes as large as those encountered on the engine.

While it is impossible to obtain exact duplication of all of these conditions, it is not difficult to attain a compromise

Figure 16. Differential-starter-vibration spectra of *R*-2800 power-plant installations

Measured at ten inches from mounting flange



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* See the appendix for method of measuring differential vibration of accessories.

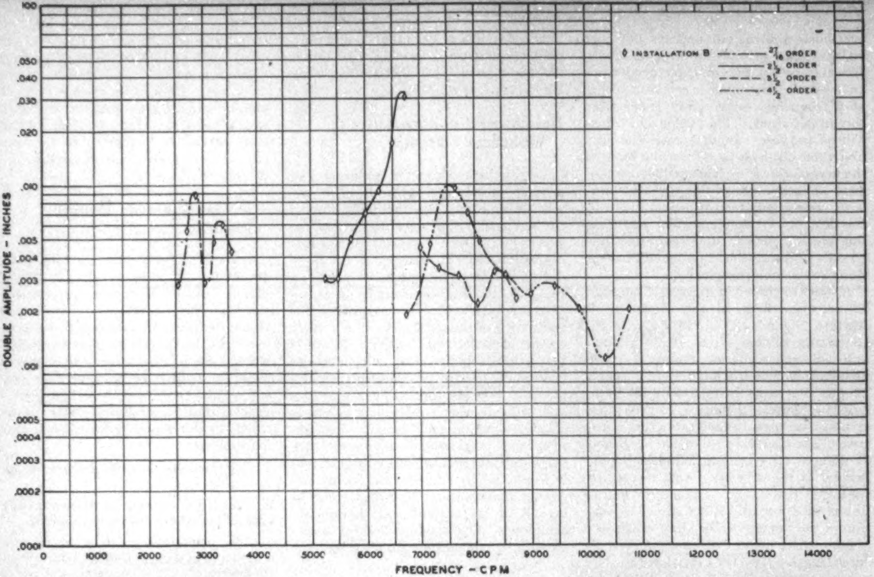
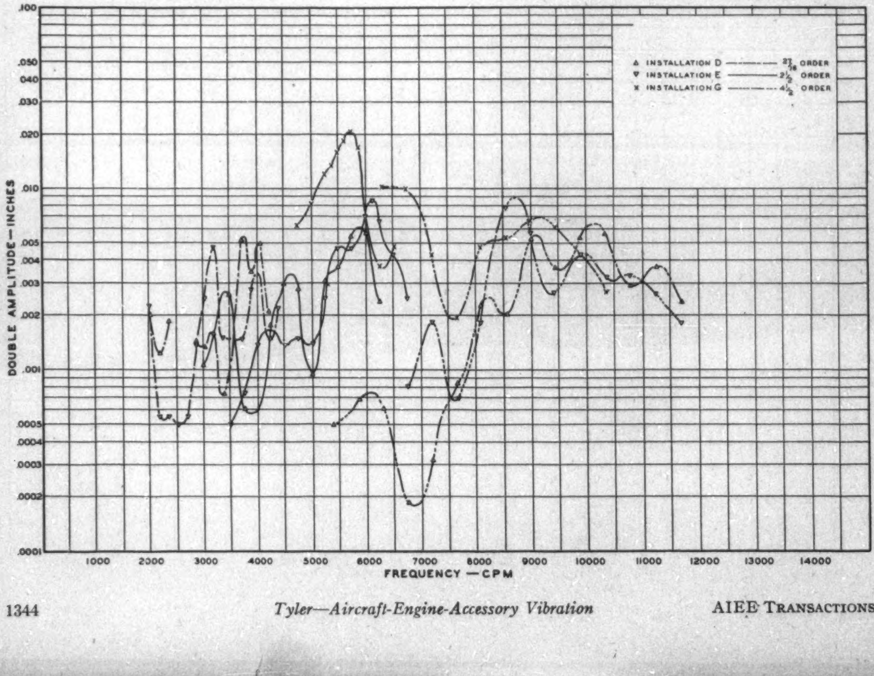


Figure 17¼ (above). Differential-generator-vibration spectra of early *R*-1830 power-plant installations

Measured at ten inches from mounting flange

Figure 18 (below). Differential-generator-vibration spectra of revised *R*-1830 power-plant installations

Measured at ten inches from mounting flange



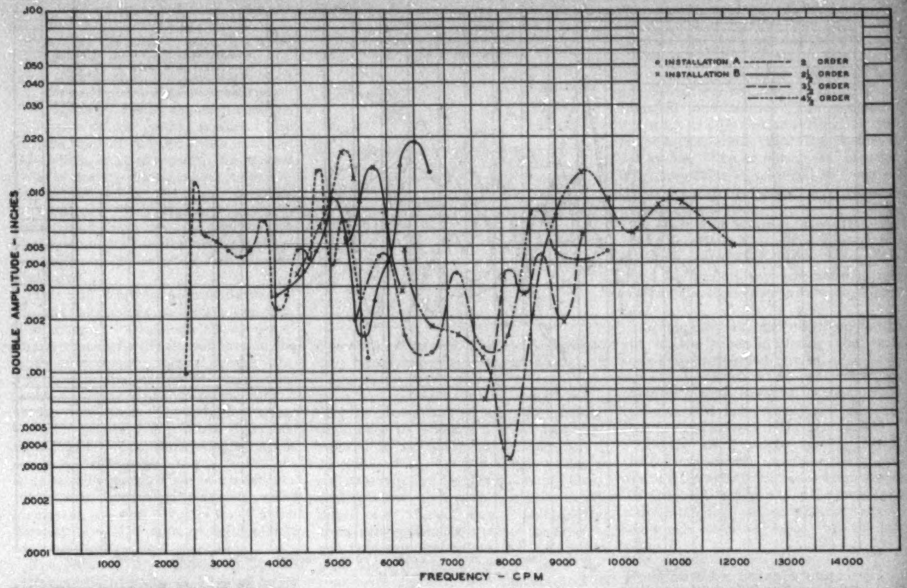
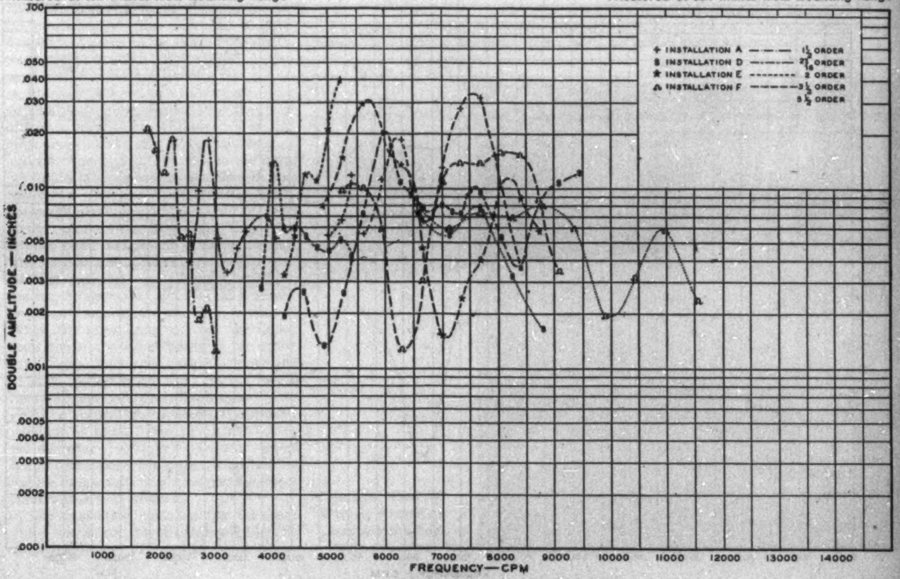


Figure 19 (above). Differential-generator-vibration spectra of *R*-2000 power-plant installations

Measured at ten inches from mounting flange

Figure 20 (below). Differential-generator-vibration spectra of *R*-2800 power-plant installations

Measured at ten inches from mounting flange



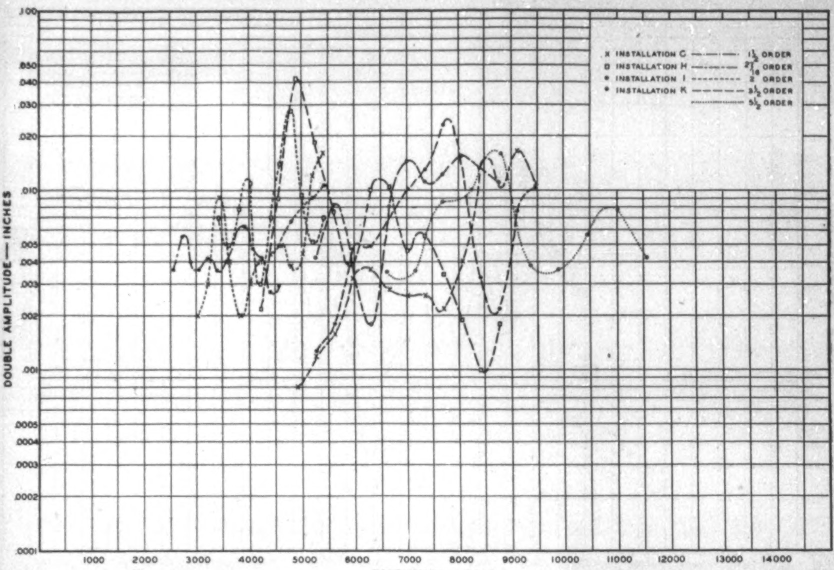
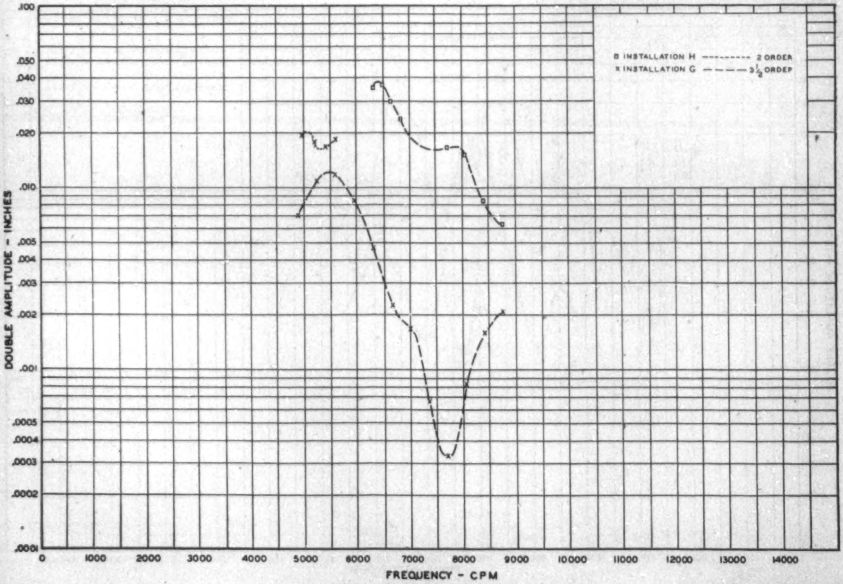


Figure 21 (above). **Differential-generator-vibration spectra of *R*-2800 power-plant installations**

Measured at ten inches from mounting flange

Figure 22 (below). **Differential-supercharger-vibration spectra of *R*-2800 power-plant installations**

Measured at ten inches from mounting flange



which is close enough for all practical purposes.

Regarding the mode of vibration to be duplicated, it may be assumed that the engine vibration is the combined pitch and yaw known as a whirl. In this mode of vibration the thrust axis of the engine describes a double conical surface. The common apex of the two cones is at the node which is at or close to the engine center of gravity during resonant engine–propeller vibration. It is also desirable to construct the accessory-vibration-testing machine so that the effect of the accessory on the location of the node of the engine–propeller whirl will also be duplicated.

Pratt and Whitney Aircraft developed an accessory-vibration-testing machine in an attempt to duplicate the modes of engine–propeller whirl encountered in the range of accessory natural frequencies. A schematic diagram of the machine is shown in Figure 23. A large mass is flexibly mounted by *R*-1830 engine-vibration isolators on a frame supported by the floor. A structure supporting the rear cover of an engine is attached to the large mass by means of a steel diaphragm. While this diaphragm prevents radial motion of the rear-cover support, it allows whirl of the support to duplicate engine–propeller vibration.

The problem of producing amplitudes of accessory vibration as large or larger than those encountered in aircraft power-plant installations was solved by introducing an adjustable-stiffness element between the rear cover and the large mass. This makes it possible to adjust the natural frequency of the machine to any desired frequency in the range of frequencies to be studied. By operating the machine at or near resonance the power requirement is kept to a minimum. The adjustable-stiffness element is in the form of four arms with one end pivoted on the large mass at the rim of the diaphragm and the other end able to slide in the form of circular arcs on tracks attached to the rear-cover support. When these arms are perpendicular to a line from the node at the center of the diaphragm, they contribute their maximum stiffness to the whirl deflections of the rear-cover support. In this position the arms are straight tension–compression springs and are used at their maximum effective stiffness. When the arms point toward the node, they contribute no stiffness to the whirl deflections, and the natural frequency in whirl is dependent only on the bending stiffness of the diaphragm. The range of natural frequencies of the present machine is from 5,000 to 12,000 cycles per minute. However, a heavier set of arms would raise the upper limit, and a lighter diaphragm would reduce the lower limit if this were desired.

The excitation problem for this machine was solved by the General Electric Company motor division at Lynn, Mass.

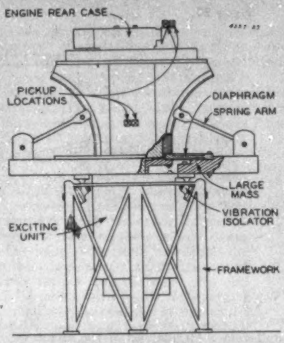


Figure 23. Pratt and Whitney Aircraft accessory-vibration-testing machine

The usual method of using a rotating unbalance to produce a whirl was calculated to be quite cumbersome for a machine which handled forces as large as those required for this machine. General Electric Company's solution consisted of a "shaker" motor built mostly from conventional a-c motor parts with the field supported from the large mass and the armature supported from the rear-cover support. The field consists of two sets of windings, one set excited by direct current and the other set by an alternating voltage obtained from an alternator driven by a variable-speed motor.

The armature is not allowed to rotate; in fact the torque is essentially zero, and only a rotating lateral force exists between the armature and the field which causes a whirling motion of the structure.

This is a very convenient solution to the problem of obtaining a large rotating unbalance force without any rotating unbalanced mechanisms. The excitation frequency is adjusted by setting the rpm of the a-c generator which is driven by a d-c motor. The amplitude of vibration of the machine is adjusted by varying the alternating and direct field currents in the shaker motor.

A Pratt and Whitney Aircraft accessory-vibration-testing machine which has sufficient excitation capacity to produce rapid failures in any conventional aircraft-engine accessories is shown in Figure 24.

Although several bugs had to be worked out of the machine during the early stages, it has turned out to be a fairly successful tool (about 95 per cent of the operator's time is spent in testing accessories and rear covers and five per cent in maintenance of the machine).

Shortly after fatigue testing of accessory–rear-cover combinations was started, the noise level became a problem. This problem was solved by the means of a soundproof hood suspended from the ceiling which could be placed over the machine.

Procedure for Establishing Accessory Strength Requirements

The "logical" procedure for vibration-testing an aircraft accessory is as follows:

1. Adjust the frequency of vibration of the engine rear cover to the natural frequency of vibration of the accessory.

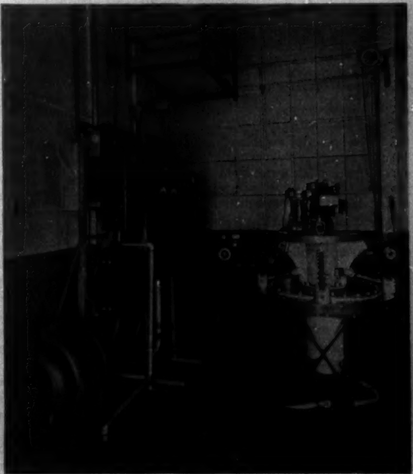
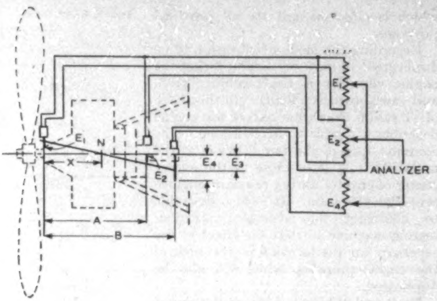


Figure 24. Pratt and Whitney Aircraft accessory-vibration-testing machine

Figure 25



2. Increase the amplitude of the engine rear cover until it equals the maximum amplitude which can be encountered in a power-plant installation (see Figures 4 through 8 for quantitative data*).

3. Slide a wide basket under the machine to catch the pieces of the accessory as they fly off.

It so happens that even if the accessory were strong enough to withstand at resonance the engine vibration encountered at an engine–propeller resonance, the accessory mounting studs would fail. In the light of this experience, the testing engineer would decide quickly that the procedure which at first seemed logical is definitely not the most practical.

Another procedure for accessory-vibrating testing, one which reflects the "philosophy of design" mentioned at the beginning, has been recommended by Pratt and Whitney Aircraft for several years and is now being followed by several accessory manufacturers. This procedure is as follows:

1. Adjust the frequency of the engine rear cover to a point near the natural frequency of vibration of the accessory.

2. Increase the amplitude of the engine rear cover until the double amplitude of vibration of the accessory (at a point ten inches from the mounting face) with respect to the rear cover* is 0.010 to 0.015 inch.

3. Run a fatigue test. If the accessory fails, "beef" it up and rerun the test. Continue this process until the studs in the rear cover fail before the accessory fails.

The explanation of the philosophy on which this procedure is based is given in the following paragraphs.

In the discussions among airplane, accessory, and engine manufacturers regarding strength requirements of the parts involved in the attachment of an accessory to an aircraft engine, the following procedure has been established:

1. If the accessory fails, it is up to the accessory manufacturer to fix it.

2. If the accessory mounting studs fail, there are three alternatives:

(*a*). Change the accessory, usually by "beefing" it up until its natural frequency is moved far enough away from the engine–propeller resonance to reduce the excitation at its new natural frequency.

(These changes in natural frequency sometimes are made inadvertently as a result of a design change made for some other reason.)

(*b*). Use another accessory.

(*c*). Brace the accessory from the edges of the rear cover.

The one solution that is neither resorted to nor even suggested is a change in the size or pattern of the mounting studs. This is understandable since any change in dimensions affecting interchangeability would require a special engine as well as a special accessory for that particular installation. An analysis of the accessory manufacturer's problem will show that:

(*a*). There will be no advantage to him in having his accessory stronger than the mounting studs, because, if the vibration is severe enough to require this extra strength, it will have to be braced anyway to save the studs.

(*b*). If the accessory is not so strong as the studs, the accessory manufacturer may suddenly find himself with an installation where a propeller change has rendered unsatisfactory thousands of accessories which had previously given satisfactory service for a long time on a particular installation.

(*c*). The only practical solution is to develop the accessory to have the same fatigue strength as the mounting studs, with a little extra for a factor of safety.

In the preceding paragraphs, it has been implied that the *AN* standard mounting studs for starters and generators are not strong enough to withstand the vibration which may be encountered in normal engine–propeller–accessory installations. It should be explained, therefore, that when the natural frequency of an accessory occurs right at the peak of one of the main whirling modes of the power plant nothing can keep it and/or the mounting studs from vibrating to pieces. In one airplane installation it was only possible to pass quickly through the resonance speed a few times before the starter was dangling in the accessory compartment with all of the studs broken. While this exact alignment of the accessory resonance with the engine–propeller resonance happens only once in a lifetime, an installation with close alignment of these frequencies happens all too frequently.

The use of conventional accessories (even though they are as strong as the mounting studs) will be satisfactory vibrationwise on most of the assembled engine–propeller combinations. When close alignment occurs between the accessory natural frequency and the engine–propeller resonance, stud failures will be encountered.

The weight times distance of the center of gravity from the mounting face, that is, overhang moment, is an index of the natural frequency of an accessory on an engine rear cover, provided that the accessory stiffness and the proportions remain approximately the same as other accessories which are used for reference. Pratt and Whitney Aircraft recommended the use of overhang-moment limits at a time when 14 was the maximum number of cylinders and when 28 to 32 pounds was the upper limit of accessory weights. If these overhang-moment limits had been applied and maintained, vibration troubles on 14-cylinder engines could have been prevented, provided sufficient stiffness was incorporated in the accessories. Practically all starters, generators, and cabin superchargers are so heavy that their natural frequencies are now in the range of the 14-cylinder engine–propeller natural frequencies, and furthermore we now have to contend with the higher frequencies of the 18-cylinder engines. An overhang-moment limit large enough to allow the use of present accessories is not a remedy. If the accessory vibrations are too large as determined by ground and flight vibration tests, a job of tailoring will be required to unscramble the accessory vibration from the engine–propeller vibration.

Remote-Mounted Accessory Gearbox

The vibration level of an airplane structure is negligible in the frequency range between 5,000 and 12,000 cycles per minute compared with that encountered on the engine, particularly if a modern type of engine-vibration isolator is used. An accessory gearbox driven by an extension shaft and mounted on the fire wall has several advantages from the standpoint of space required, accessibility, flexibility of arrangement, flexibility of gear ratios, and so on. There is also an advantage from the standpoint of accessory vibration which needs no explanation.

The advantage of weight saving resulting from the lack of vibration is not possible if the present engine accessories are used on the remote-mounted accessory gearbox. In order to make this weight saving possible the accessories must be redesigned as airplane accessories which would involve reverting to magnesium housings and flanges. A detailed analysis of the gearbox installation is beyond the scope of this discussion, but it is believed that the accessory-vibration problem will be solved once and for all, when and if accessory gearboxes are mounted on airplane structure.

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* See the appendix for method of measuring the vibration of an accessory with respect to the engine.

Summary

1. The overhang-moment limit, or still better a limit on the moment of inertia of the accessory about a line through the center of the accessory mounting surface, would be helpful by limiting one of the two factors which control the natural frequency of the accessory–rear-cover combination. However, the natural frequency is the factor of prime importance, and, if control of the natural frequency is to be maintained, it should be applied directly. This control is not practical with the equipment now in use, because accessories have grown so large that sufficient stiffness cannot be built into their supporting structures to keep their natural frequencies out of the range of the engine–propeller natural frequencies.

2. A survey of accessory-vibration data obtained on recent and current aircraft power-plant installations indicates that chance is the most important factor in determining the suitability of an accessory installation. Practically all accessory installations have natural frequencies in the frequency range where engine–propeller resonances exist. If an engine-accessory natural frequency coincides with an engine–propeller resonance in which large amplitudes of vibration occur, failures will result in the accessory and/or the accessory mounting studs. To obtain a remedy for these unfortunate installations, either some revision of the installations is required, which results in its being nonstandard, or some other accessory or accessories must be used.

3. Accessory development is facilitated greatly by the use of an accessory-vibration-testing machine. An aircraft-engine rear cover should be mounted on the machine for attachment of the accessories. This machine should produce vibration reasonably similar to the vibration produced in the power-plant installation. Exact duplication of the vibration of a particular installation is not necessary during general development work, since the vibrations of various installations differ appreciably. The general design features of an accessory-vibration-testing machine developed by Pratt and Whitney Aircraft have been presented with this discussion.

4. Accessory manufacturers should keep a record of the vibration characteristics of each power-plant installation on which their accessories are used. Whenever a change is made in the propeller, the gear ratio, or another accessory on the same installation, the accessory manufacturer should be notified by the airplane manufacturer. He also should be supplied with a copy of the data on the vibration characteristics of the revised installation as soon as possible, so that he may determine whether his accessory will be adversely affected. If there is any question, he should check same by running endurance tests on an accessory-vibration-testing machine.

5. The only possibility of any real improvement in the accessory-vibration situation in future installations seems to be through the use of remote-driven accessory gearboxes mounted on the airplane structure.

Appendix. Technique for Measuring Accessory Vibration

The amplitude of vibration of the accessory is measured at a point ten inches from the mounting surface of the accessory by means of a three-pickup differential circuit as shown in Figure 25. This measurement gives the amplitude of vibration of a point on the accessory with respect to an imaginary point on the engine extended to the location of the vibration pickup on the accessory and takes into account phase differences between engine and accessory vibration. A reading of the sum of all orders of vibration present is desirable as well as a reading of each order separately, so that an analysis can be made of the vibration characteristics of the accessory installation.

The point ten inches from the mounting surface of the accessory was selected as a standard attachment point for vibration pickups. In many installations it is not possible to mount the pickups in this exact location because of interferences. In these instances the pickups are located as close to ten inches from the mounting surfaces as possible.

In Figure 25, A is the distance between the front and rear pickups, and B is the distance from the front pickup to the pickup on the accessory. Since A and B are constants for a given installation, a circuit, as shown, may be set up to measure "differential" or relative vibration. For the whirling modes of vibration the engine vibrates at resonance about a node N. The pickups generate voltages E_1, E_2, and E_4, which represent the vibration at the nose of the engine, at the rear of the engine, and at the accessory respectively. (E_1 is negative because of phase relationship.) E_3 is a synthetic voltage representing the vibration of the engine in the plane of the accessory pickup and may be derived as follows:

$$-\frac{E_1}{E_2}=\frac{X}{A-X}$$

$$-\frac{E_1}{E_3}=\frac{X}{B-X}$$

$$\frac{E_3}{E_2}=\frac{B-X}{A-X}=\frac{B-A\,\dfrac{E_1}{E_1-E_2}}{A-A\,\dfrac{E_1}{E_1-E_2}}$$

$$E_3=\frac{B}{A}\,E_2=\frac{B-A}{A}\,E_1$$

The differential vibration is proportional to

$$E_4-E_3=E_4-\frac{B}{A}\,E_2+\frac{B-A}{A}\,E_1$$

Prior to measuring the differential vibration, the potentiometers are set according to the fractions B/A and $(B-A)/A$. Since B/A is greater than 1, it is impossible to set a potentiometer for $B/A \times E_2$. However, this may be taken care of by calling this 100 per cent, that is, by setting the potentiometer to its full resistance and reducing the resistance settings of the other potentiometers by the factor $1/B/A$ or A/B. It must be remembered that the calibration constants of the pickups also must be changed by this factor. The three-pickup-differential circuit measures only the vertical vibration. To define the vibration completely, three additional pickups are necessary in the corresponding positions to measure vibration in the horizontal plane.

An Eight-Year Investigation of Lightning Currents and Preventive Lightning Protection on a Transmission System

Discussion and authors' closure of paper 44-50 by E. Hansson and S. K. Waldorf, presented at the AIEE winter technical meeting, New York, N. Y., January 24–28, 1944, and published in AIEE TRANSACTIONS, 1944, May section, pages 251–8. Other discussion presented at the winter meeting appears on pages 463–6.

E. C. Starr (Oregon State College, Corvallis, Oreg.): Mr. Hansson and Mr. Waldorf have made a further addition to the growing fund of information on lightning currents, and they have made a careful analysis of the results of preventive lightning protection as practiced on their system. Additional useful information can be obtained from the data they have presented and from other relative data probably available in their files.

TOWER CURRENTS

If the footing resistance (surge value) of each tower from which a current measurement has been obtained is known, the approximate value of the current in the stroke to the tower can be obtained. The current in the same stroke, if it had occurred to a zero-resistance ground, also could be estimated. If overhead ground wires are present and the span length is such as to give a propagation time equal to at least 50 per cent of the assumed time to crest of the high-current portion of the stroke (750-foot spans minimum for a 1.5-microsecond front), the total stroke current is

$$I_0' = I_t + \frac{2E}{Z} = I_t\left(1+\frac{2R}{Z}\right) \tag{1}$$

$$= \frac{E_s - E}{Z_s} \tag{2}$$

where

I_t = tower current, amperes

E = tower voltage due to ground-resistance drop, volts

Z = combined surge impedance of ground wires; approximately 500 ohms for one, 312 ohms for two (corona neglected)

R = tower-footing resistance, ohms (surge)

E_s = potential lowered to tip of leader stroke, progressing relatively slowly, prior to final contact; volts

Z_s = equivalent surge impedance of stroke channel during high-current period; approximately 325 ohms

Then the full-stroke current, if the discharge had occurred to a ground terminal of zero-resistance, would have been

$$I_0 = \frac{E_s}{Z_s} = I_0' + \frac{E}{Z_s}$$

$$= I_t\left[1+R\left(\frac{2Z_s+Z}{ZZ_s}\right)\right] \text{ amperes} \tag{3}$$

Equations 2 and 3 are based on the theory that the highly charged, conducting stroke channel is lowered toward the earth relatively slowly until final contact is made with the ground terminal. The essential characteristics of a long highly charged conductor or cable discharging to ground or to the sheath at one terminal then are closely simulated. The conventional traveling wave analysis therefore can be applied to obtain an approximation of the initial performance characteristics of the circuit upon which the lightning stroke terminated.

By means of equation 1 the actual total maximum current in the stroke to the tower can be computed. The results obtained will be accurate only when the span length is sufficient to permit the current to rise to crest value before ground reflections have returned from the first adjacent towers. For the 1.5-microsecond front, which seems to be a good average value for the severe current peaks that have been recorded by cathode-ray oscillograph, the minimum span length necessary to permit the application of this equation is 750 feet. If a tower carries two ground wires, for example, and has a footing resistance of 20 ohms, the stroke current is approximately 13 per cent higher than the tower current.

For general circuit performance studies it is convenient to know the current that would have been delivered by the lightning stroke, if it had terminated on a ground of zero resistance. This value can be estimated by means of equation 3. Careful study of available data indicates that the value of Z_s is about 325 ohms, and the value of Z for low footing resistances is about 500 ohms for one ground wire and 312 ohms for two ground wires. It would be interesting as well as valuable for use in further studies to have the current distribution curve of Figure 2 of the paper modified by the application of equation 3. For a footing resistance of 20 ohms, I_0 is approximately 19 per cent greater than I_t and for 75 ohms the increase is approximately 71 per cent. As the resistance approaches zero I_0 becomes equal to I_t.

APPARENT SURGE IMPEDANCE OF OVERHEAD GROUND WIRE

In Table X of the paper the authors have evaluated the maximum apparent surge impedance of an overhead ground wire. The values obtained are very low and a different method of analysis has given results that agree more closely with conventional values. If the lightning-stroke channel impedance and charge characteristics discussed above are applied to strokes to ground wires in the mid-span zone, the following relationships are obtained. The ground-wire potential at the point of stroke contact, before the return of ground reflections from the nearest towers, is approximately:

$$E = I_t\frac{Z_1}{2}\left[1+R\left(\frac{Z_1+2Z_2}{Z_1Z_2}\right)\right] \text{ volts} \tag{4}$$

where

Z_1 = surge impedance of single ground wire in affected span; reduced below low-voltage value by large corona sheath prior to high-voltage spark-over
$\cong$450 ohms

Z_2 = surge impedance of ground-wire assembly outside affected span
$\cong$312 ohms for two ground wires

This simple equation does not include the influence of the second ground wire in the affected span but gives results only approximately 1.5 per cent high for tower footing resistances of 20 ohms and approximately 4 per cent low for resistances of 75 ohms. If $R = 20$ ohms, equation 4 becomes

$$E = 255\ I_t \text{ volts} \tag{5}$$

For spans 800-feet long, if the 1.5-microsecond wave front is assumed, equation 5 becomes

$$E = 197\ I_t \text{ volts maximum} \tag{6}$$

The voltage to ground appearing at mid-span is in the form of a wave rising to the above maximum value in 0.8 microsecond, at which time it is effectively chopped by ground reflections and drops rapidly toward zero. Because of the coupling factor of approximately 0.25 between the ground wire and nearest conductor, the voltage stress across the air gap between the ground wire and conductor at the point of stroke contact becomes

$$E_s = 0.75 \times 197\ I_t = 147.5\ I_t \text{ volts max} \tag{7}$$

The larger current for the first item in Table X of the paper substituted in equation 7 gives 5,340 kv or 93.6 per cent of the listed spark-over value when corrected to 0.8 microsecond. Spark-over, therefore, should not have occurred, and this is in agreement with the records. Equation 4 indicates that footing resistances below 20 ohms would have further reduced the mid-span stress for the tower currents observed, and a slower wave front would have had a similar effect.

This analysis applied to item 10 of Table X of the paper indicates that the maximum tower current recorded would have resulted from a mid-span stroke that would have produced a voltage stress equal to 89.5 per cent of the listed spark-over value. Footing resistances and wave fronts different from those listed in the previous case would have similar influences on the final results in this case also.

The same analysis applied to item 17 of Table X of the paper, allowing the greater spark-over time permitted by the longer span, indicates that the listed spark-over voltage should have been exceeded by approximately 2 per cent. This condition is again contingent upon the footing resistances and current wave front having been of the orders of those chosen for the previous examples. It will be observed that the above results, which, so far as can be determined, are in agreement with field records, were obtained by employing normal values of ground-wire surge impedance.

PROBABILITY OF MID-SPAN SPARK-OVER

The approximate probability of mid-span spark-over in the foregoing examples can also be computed. For a stroke to mid-span, the full-stroke current or the current that would have resulted had the same stroke contacted a terminal of zero resistance is

$$I_0 = E\left(\frac{2Z_s+Z_1}{Z_1Z_s}\right) \text{ amperes} \tag{8}$$

For the circuit quantities used previously equation 8 reduces to

$$I_0 = 7.5\ E \text{ (kv) amperes} \tag{9}$$

If a span length of 1,000 feet, a wave front of 1.5 microseconds, and a coupling factor of 0.25 are assumed, the mid-span air-gap stress becomes

$$E_s = 0.9 \times 0.75\, E = 0.675\, E \qquad (10)$$

The factor 0.9 is the per unit value of the current wave in one microsecond, the time of arrival of the chopping reflection. The full stroke current and the one-microsecond sparkover voltage are then related by the equation:

$$I_0 = 11.1\, E_s \text{ (kv) amperes} \qquad (11)$$

If the spark-over voltage listed in item 10 of Table X in the paper is substituted in equation 11, a value for I_0 of 106,000 amperes is obtained. If it is assumed that the currents of Figure 2 in the paper were obtained from strokes to towers having an average resistance of 20 ohms, the correction factor of 19 per cent applies as already discussed, and the full stroke current of 106,000 amperes is the equivalent of 89,500 amperes on the distribution curve in Figure 2. Approximately 0.2 per cent of the strokes to 20 per cent of the line having spans of 1,000 feet and footing resistances of 20 ohms might therefore be expected to produce mid-span spark-over. Since there is an average of 113 strokes per 100 miles of line per year on the system studied by the authors, the above spark-over probability amounts to approximately one per 100 miles in 22 years of operation.

Inasmuch as such a spark-over would immediately precipitate spark-overs on the same conductor at the nearest towers, the ensuing power arc, if one developed, would be expected essentially always to choose the shorter path over the insulators at the towers where the arc voltage drop would be very much less. The reported observations of flashovers at towers and not at mid-span are in good agreement with this reasoning.

A similar analysis for item 1 of Table X of the paper indicates a mid-span spark-over probability for such a line of approximately one per 100 miles each 5.5 years of operation.

This analysis applied to item 17 of Table X indicates a mid-span spark-over probability for such a line of one per 100 miles each 29 years of operation.

From the results of these analyses, it is not surprising that the tower currents listed in Table X, even though they are all well toward the low-percentage end of the distribution curve in Figure 2, failed to produce a spark-over or at least a spark-over that was followed by a power arc and line outage.

MID-SPAN STROKE CURRENTS

Equation 4 substituted in equation 8 gives an expression for the full-stroke current I_0 for strokes to mid-span in terms of the current in the nearest towers and the footing resistances of these towers. This expression, which again is in approximate form, is:

$$I_0 = I_t\left(1+\frac{Z_1}{2Z_0}\right)\left(1+\frac{Z_1+2Z_2}{Z_1Z_2}R\right)\text{amperes} \qquad (12)$$

and makes possible an estimate of the potential full-stroke current from adjacent tower current readings. The slight errors in this equation are the same as those given for equation 4 and result from the same simplification. Equation 12 applies only when the propagation time over the span length is not less than approximately two-thirds the time to crest of the current wave. For a 1.5-microsecond wave front and spans of 1,000 feet, the additional correction factor is approximately 2 per cent additive. That is, the true value of the full stroke current is 1.02 times the value obtained from the previously corrected equation, the over-all error being negligible.

For footing resistances of 75 ohms, $I_0 = 2.7\, I_t$; for 20 ohms, $I_t = 2.0\, I_0$, and for footing resistances of zero ohms, $I_0 = 1.7\, I_t$. It is apparent that the direct addition of tower currents believed to have been caused by the same stroke, especially if it were a near mid-span stroke, may give erroneous results. For very low footing resistances, the sum of the adjacent tower currents for a mid-span stroke is considerably higher than the full value of the stroke that produced these currents, even if it occurred to a ground terminal of zero resistance.

I should like to ask the authors if there were instances on protected lines, particularly on their excellently protected 220-kv system, where tower-footing resistances were such that measured tower currents multiplied by independent tower-footing resistance would indicate that insulator spark-over voltage had been reached. The spark-over voltage for the 1 x 5-microsecond wave is referred to.

E. Hansson and **S. K. Waldorf:** In commenting on Professor Starr's excellent analysis, the authors wish to state again that their "apparent surge impedance" of an overhead ground wire is not intended as an evaluation of the theoretical surge impedance, but is only a practical factor which might be applied to a crest current to obtain an estimate of the voltage at mid-span.

In reply to the question about the recording of any tower potentials exceeding the insulator flashover level, there have been none. The approximate 1 x 5-microsecond flashover value of the insulators on the 220-kv lines is 2,000 kv.

Measured Electrical Constants of 270-Mile 154-Kv Transmission Line

Discussion and author's closure of paper 44-80 by C. A. Streifus, C. S. Roadhouse, and R. B. Gow, presented at the AIEE North Eastern District technical meeting, Boston, Mass., April 19–20, 1944, and published in AIEE TRANSACTIONS, 1944, July section, pages 538–42.

C. F. Wagner (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The principal value of this paper lies in the comparison between measured and calculated values of the constants of transmission lines. Naturally, therefore, the authors should be explicit in defining the assumptions made in the calculations. Frequently in calculating the characteristics of transmission lines the separation of conductors is taken as the values at the tangent towers. The authors in this instance have gone beyond this assumption and have averaged the separation, taking into consideration the difference in sag of the ground wires and conductors. Further clarification of the method of averaging is desirable. A statement from the authors is also desirable regarding the assumption used in determining the length of conductors. Did they use merely the geographical length of the line, or did they compute the constants on the basis of the actual length of the conductors, taking the sag into consideration? Further clarification is also desirable regarding the basis for using an earth resistivity of 100 ohms per meter cube.

Eric T. B. Gross (Cornell University, Ithaca, N. Y.): Freedom from higher harmonics on the system during tests cannot be assumed implicitly. Harmonics may be significant factors, and it appears worthwhile whenever possible to use oscillographs to check wave shapes of currents and voltages. Small magnitudes of higher harmonics in the voltage easily can lead to a larger rms value of current. It must be kept in mind that the line discussed in the paper is long, in fact, longer than a full wave length for some of the higher harmonics. It seems, that possibly higher harmonics are partially responsible for the difference in the results derived from voltage values and from current values only. This may help to explain some minor differences which appear in measurements.

The paper is a valuable addition to the literature concerning long-line data. Considering the fact that instrument readings are the basis for the measured values, I should think that the "measured" values and the "computed" values check better than could be expected.

Some simple and satisfactory data about impedances of conductor-and-ground circuits can be found in a paper by Rüdenberg[1] and a book by Ollendorff[2] which are known more widely in Europe, and these may be of interest to some of our colleagues.

REFERENCES

1. THE DISTRIBUTION OF GROUND CURRENTS IN A-C EARTH-RETURN CIRCUITS. R. Rüdenberg. *Zeitschrift fuer Angewandte Mathematik und Mechanik* (Berlin, Germany), volume 5, 1925, page 361.

2. EARTH-RETURN CIRCUITS (book), F. Ollendorff. Verlag J. Springer, Berlin, Germany, 1928. Chapter VIII, pages 108–50.

E. A. Church (Boston Edison Company, Boston, Mass.): This paper demonstrates that the positive-sequence constants of long lines may be computed from the circuit configuration with sufficient accuracy for all purposes for which they are needed.

The zero-sequence constants, which depend to a large extent on the resistivity of the earth-return path including counterpoise if such is installed, are not so easy to calculate accurately. If it were possible to determine the average resistivity of the earth-return path, it would be possible to compute the zero-sequence constants of both long and short lines with the necessary accuracy for relaying calculations. This resistivity may vary over wide limits over the territory which a long line may traverse. Also where this resistivity is high, the current may penetrate to depths where the resistivity may be quite different from what it is at the surface. In order to simplify the calculations of zero-sequence constants, it is necessary to assume a homogeneous earth of infinite depth over the whole length of the line, a condition which will not be obtained in practice. Thus the only sure way of obtaining accurately the zero-sequence constants is by test. Even this may not be accurate when the constants for the whole line are reduced to a per unit length basis, if the line traverses territory with widely different ground resistivities. These errors, of course, will be reduced by ground wires and counterpoises which carry part of the zero-sequence current and which have definite dimensions and distances from the conductors.

On short lines high terminal resistances may cause large errors in the earth-return resistance per unit length, if the resistance as measured is divided by the length of the line. This terminal effect does not have such a great effect on the reactance. In fact the tested zero-sequence reactance of a line can be used to determine with reasonable accuracy the average earth resistivity of the territory over which the line traverses.

A joint report[1] of the Edison Electric Institute and the Bell Telephone System is an additional valuable reference on this subject.

REFERENCE

1. COMPUTATION OF ZERO-SEQUENCE IMPEDANCES OF POWER LINES AND CABLES, Joint Subcommittee on Development and Research of the Edison Electric Institute and the Bell Telephone System. Technical report 2J-8, February 27, 1936.

C. A. Streifus: With regard to questions on assumptions raised by Mr. Wagner, average vertical separation between conductors and shield wires and average distance of conductors above ground were determined by first drawing to scale approximate sag curve of shield wires and conductors for a typical ruling span of 600 feet from sag-tension data specified for line construction. The sum of the vertical distances at equally scaled intervals, divided by the number of these distances, gave average vertical distances shown in Figure 2 of the paper. While this method is approximate and cannot include all effects of differences in length of spans, angles, and perhaps slight differences in tensions of conductors, it gives better values than the conventional method of using spacings at straight-line structure. Geographical length of line was used in computing theoretical constants. Because of lack of specific data on earth resistivity for the country traversed by the line, 100 meter ohms, considered average, was used. As pointed out in paper, it is possible that actual resistivity may be less.

Possible effect of harmonics, mentioned by Professor Gross, was considered in preparation for the tests. During measurements the 2,400/4,160-volt power source was the wye-connected winding of a delta-wye transformer bank energized from a large system providing low total-input impedance as compared with impedance of the line under test. The system wave form is free enough from harmonics to permit system-wide joint use of poles on distribution lines for both power and communication circuits. For these reasons the authors are confident that in respect to harmonics the tests were made under conditions representative of or even better than commercial operation of the line.

Cathode-ray oscilloscope and magnetic oscillograph were not available for these tests.

Current Rating of Cables as Affected by Mutual Heating in Air or Conduit

Discussion and closure of paper 44-53 by the research committee of the Insulated Power Cable Engineers Association, presented at the AIEE winter technical meeting, New York, N. Y., January 24–28, 1944, and the North Eastern District technical meeting, Boston, Mass., April 19–20, 1944, and published in AIEE TRANSACTIONS, 1944, pages 354–65. This discussion was presented at the Boston meeting; discussion of this paper at the winter meeting appears on pages 424–6.

E. A. Church (Boston Edison Company, Boston, Mass.): This paper is very instructive in giving the effect of grouping in various ways on carrying capacity of cables. However, it appears that all the tests were made with cables in air or enclosed in conduits which were exposed to air where convection currents account for a part of the heat dissipation.

An equally important problem is determining the effect of mutual heating with ducts embedded in concrete floors or in the earth where convection is not effective to as great an extent. The effect of mutual heating is much more pronounced in these cases in reducing current ratings among large concentrations of ducts.

Recently there was occasion to determine the proper separation of the ducts in a bifurcated 16-duct conduit line to be located within the confines of a public street in order to obtain the best over-all balance in cost per unit of transmitted power. The effect of close proximity of the two sections was quite surprising. The results of the computations are shown in Figure 1. The computations were made, using the well-known method of geometric mean distances and mutual resistances to heat flow.

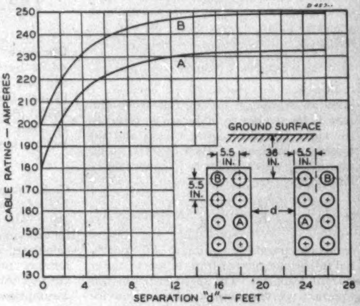

Figure 1. Computed variation in current-carrying capacity as a function of distance between branches of a bifurcated 16-duct conduit

A—Rating based on copper in hottest duct, not exceeding 82 degrees centigrade

B—Rating based on copper in coolest duct, not exceeding 82 degrees centigrade

Ambient temperature = 20 degrees centigrade

All ducts were equally loaded with 350,000-circular-mil 15-kv type-H cable

Loss factor = 60 per cent

Since in this particular case it was no more expensive to locate the two eight-duct sections one on each side of the street than to separate them more than four feet, that procedure was followed; thereby obtaining the maximum segregation from both a current capacity and a reliability standpoint.

In general it can be concluded from these results that large concentrations of ducts carrying appreciable currents are not economical except where space limitations dictate them.

It would be interesting, if the results of these computations should be checked by tests similar to those described in the paper.

Robert J. Wiseman (The Okonite Company, Passaic, N. J.): Mr. Church's comments are quite helpful in bringing out the factors that influence the rating of cables under various conditions of installation. We need more of such information and I hope that others will contribute data as opportunity permits.

It is true and was intended that all the tests on conduits were conducted in air. Because of convection currents we were not able to calculate accurately the mutual heating effect of groups of conduits. The results had to be obtained experimentally. Ducts embedded in concrete floors are assumed subject to the same conditions as ducts in the earth, and the same heating constants are taken. Perhaps some utility company might collect data for us and find out how correct we are in our assumption.

Mr. Church's reference to the value of splitting up a 16-duct line into two eight-duct lines economically is logical, yet we still find cases where duct lines are built with more than eight or nine ducts. If a similar study were made each time new duct banks were contemplated, the same conclusions would be made, and in time more efficient loading of duct banks would take place as Mr. Church indicates.

There is an economic limit also to the number of conduits in a group. The reduction factors given in Table IX of the paper may be used when a study is made of the kilovolt-ampere cost of distributing power in a group of conduits.

Prevention of Rotor-Winding Deformation on Turbogenerators

Discussion and author's closure of paper 44-85 by John G. Noest, presented at the AIEE North Eastern District technical meeting, Boston, Mass., April 19–20, 1944, and at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944, and published in AIEE TRANSACTIONS, 1944, July section, pages 514–19. The first four discussions were presented at Boston; the fifth at St. Louis.

H. D. Taylor (General Electric Company, Schenectady, N. Y.): Mr. Noest's paper is a most interesting one and shows evidence of the great deal of thought which he must have given this intricate problem of rotor-winding distortion. Although, from a practical point of view, the general nature of this problem seems to be fairly well understood, I believe it is not susceptible to precise mathematical solution on the basis of available data. There are many other ramifications beyond those treated by Mr. Noest, including such points as: the nonuniform lengthwise distribution of compressive stress caused by partial slipping of conductors on the wedges; the variation in temperature along the length of the coils (which may be quite as important as the variation through their radial depth); and the quantitative evaluation of creep effects (or, more properly, relaxation effects).

One rather obvious way to minimize this deformation is to wind the rotor with conductors made of materials having more favorable physical properties than those previously used. Hard-drawn copper is, in a measure, such a material, although it is handicapped by some unfavorable characteristics, one of which is the tendency to lose its greater mechanical strength gradually over a period of years when subjected to even moderately elevated temperatures. It is known that the addition of some other alloying element such as silver, in very slight percentage, tends to prevent this loss of strength without greatly affecting the conductivity. The company with which this writer is associated is accumulating both laboratory data and practical experience in the use of hard drawn copper suitably alloyed, so as to be in a position as soon as possible to have as good a solution to the problem as can be found in improved materials.

The preheating practice described by Mr. Noest seems to be an excellent method for preventing distortion due to restrained expansion. However, it seems to me that in pioneering this operating practice, he may have gone a bit further than really necessary in outlining a complete and detailed procedure. Recognizing as the real objective, the partial expansion of the coils before they are tightly restrained by centrifugal force, I am inclined to question the need for using full ventilation during preheating. Some ventilation is desirable, to be sure, to avoid overheating of the rotor end windings which lie outside the slots under the retaining rings. But more than is necessary for this purpose, it would seem, would merely delay the desired temperature rise of the rotor winding and body. Also, I wonder if anything is really gained by the momentary shutdown to standstill, in view of the fact that if the preheating is done at 20 per cent, or less, of full speed, the centrifugal forces are of the order of four per cent or less of full speed values and hardly can restrain the coils appreciably. Incidentally, I would like to call attention to two limitations on the amount of field current that should be used for preheating:

1. The value chosen should be such that, with the degree of ventilation employed, it will not overheat the end portion of the field coils.
2. Care should be taken to avoid oversaturation of the stator-core magnetically, which, at or near full speed, could lead to stray flux inducing excessive eddy-current losses in dovetail rib and frame structures, which might cause local burning if maintained too long.

The reduction in excitation which Mr. Noest specifies as the generator comes up to speed apparently takes care of the latter very nicely.

Preheating has benefits other than prevention of coil distortion. Without preheating the full expansive force of the coil is exerted under the heavy restraint of high centrifugal forces, and there is considerable slipping of coils along the under side of wedges at each end of the core. This abrasive action produces considerable wear and tear on the insulation, which, in times past, before the development of the present high-grade insulating materials, used to be almost disastrous. We have concrete evidence that this slipping may extend inward as much as five feet from each end of the rotor body. Obviously, if a considerable part of the coil expansion takes place before the centrifugal forces come into play, the wear and tear on the insulation will be reduced, even with present high-grade materials. Preheating for this purpose was recommended in a 1924 AIEE paper by W. J. Foster, E. H. Freiburghouse, and M. A. Savage,[1] from which I quote a few words: "...the unequal expansions of the various materials of the rotor . . . undoubtedly exert mechanical strains that come with tremendous force on the insulation and must, when repeated often enough, result in disintegration. ...It is recommended: . . . if . . . the rotor at rest has become very cold, run it at low speed with excitation sufficient to bring temperature up to about 75 degrees centigrade . . . before raising speed to normal." Preheating of turbine generator rotors would seem to be a very desirable operating practice on large machines, and I am hopeful that it may be more widely adopted.

REFERENCE

1. LARGE STEAM TURBINE GENERATORS, W. J. Foster, E. H. Freiburghouse, M. A. Savage. AIEE TRANSACTIONS, volume 43, 1924, pages 1249–57.

E. F. Dissmeyer (The Commonwealth and Southern Corporation, Jackson, Mich.): The author has provided a very excellent paper on the factors which are responsible for the deformation of turbogenerator rotor windings. His consideration of the practical aspects of preheating rotor windings and his presentation of detailed calculation data undoubtedly will be helpful to others who may find it feasible to use this method for preventing excessive winding movement.

Many of us who have been interested in the design, operation, and maintenance of turbogenerators have given considerable thought to the problem of rotor-winding deformation. It has been recognized that the preheating of rotors might provide a satisfactory solution, especially in those cases where machines are on base-load operation or where the variations in the load or cooling conditions are of somewhat minor importance.

Many system and industrial applications require that the machines follow daily load cycles. Other machines operate on base load for a number of years and later are assigned to peak load or spinning-reserve type of operation. A generator which must follow the system load cycle is subjected to rather severe variations in temperature, depending upon both the kilowatt and kilovar requirements of the system. Possible variations in cooling water conditions also will affect the temperature rise of the rotor windings. For these applications the use of rotor preheating might be somewhat helpful but may not provide an adequate solution for the winding-movement problem.

We believe, however, that it is agreed that the use of rotor preheating presents many problems. These include the necessity for special equipment, extreme care in execution, and possible loss in time when the machines must be started under emergency conditions. Although rotor preheating may provide a solution for existing machines, it would seem that the problem should be kept in mind by the manufacturers' designers and that every attempt should be made to mitigate those factors which lead to rotor-winding deformation.

In the past the problem of rotor winding deformation has been of serious interest primarily to the users of large machines. Now that 3,600-rpm speeds and the use of aluminum for rotor windings are common, the problem of winding movement is of interest to practically all purchasers of turbogenerators. The use of the higher speed results in greater centrifugal forces, and the use of aluminum accentuates the amount of movement possible. The coefficient of expansion of aluminum is approximately 50 per cent greater than that of copper. Since the coefficient of expansion of steel is somewhat less than that of copper, the relative movement of aluminum with respect to the steel rotor is two or three times that which would be obtained if copper conductors were used. This together with the fact that aluminum is a poorer conductor of heat than copper indicates that the use of aluminum justifies serious consideration.

Manufacturers' designers assure us that the plasticity of aluminum is such that the deformation of the end turns should not be serious. The removal of aluminum saddles on the end turns and the use of flexible blocking all have resulted from the manufacturers' recognizing that there will be considerable movement of the aluminum conductors. These changes in design may compensate satisfactorily for the greater coefficient of expansion of aluminum. On the other hand, they may present new and possibly more serious problems. The combined experience of a complete generation of these new designs will have to be studied before we can be sure what the correct solution for the problem is.

It is extremely unfortunate that our present standards permit temperature rises up to 90 degrees centigrade for turbogenerator windings. Also, no allowance is made for the fact that the generator voltage at times must of necessity be five per cent above the rated voltage. Experience indicates that it is possible for manufacturers to design and build machines which have temperature rises considerably less than those specified in the standards, and it therefore would seem that it is within the power of the manufacturer to do much toward reducing the possibility of rotor-winding deformation. It is true that some change in temperature cannot be avoided. These effects, however, can be reduced to the point where the forces within the conductors are within the elastic limits of the material or where the actual deformation is negligible.

The manufacturers are now in the process of making new standardized designs for turbogenerators. The use of standardized designs supposedly will lead to reduced costs, better performance, and greater reliability. These are also possible due to repetitive manufacture. In the past repetitive manufacture has made it possible for the manufacturer to reduce his cost and the amount of material used and to approach more closely the design limits set forth in the standards. It would seem that an analysis of the problem of rotor-winding deformation indicates that the manufacturer should take advantage of the possibility of repetitive manufacture to reduce the temperature rise of rotor windings and thus attempt to mitigate the effect of temperature on rotor-winding movement. Past experience indicates that where duplicate machines have been purchased the temperature rise of the rotors has been reduced by as much as 20 degrees.

It therefore appears that it is possible for a manufacturer to provide designs which result in less mechanical movement of conductors and which will not be susceptible to an excessive amount of rotor-winding deformation. Repetitive manufacture also will make it possible to improve designs to the extent that it will be feasible to revise present temperature-rise and operating-voltage standards.

M. D. Ross (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Preheating of turbine generator rotors is somewhat like the weather—we have done a good deal of talking about it but, up to date, no one has done anything about it to put these ideas into concrete form. Mr. Noest is therefore to be congratulated on his very comprehensive paper on this subject. As he has pointed out, there will be some difficulty in obtaining the desired differential temperature between field winding and rotor forging on machines with internal fans; whereas in machines with separate fans, there is a means at hand to control the ventilation, as the blowers provide good ventilation at low rotor speeds. This problem with machines having internal fans has no doubt hindered the adoption of such preheating cycles in many cases.

In 1934, the company which I represent had some experience with a 7,500-kw 3,600-rpm turbine generator rotor which had undergone severe overloads and had been started and stopped each day and in which the rotor windings were badly distorted. At that time B. A. Rose of the Westinghouse Company developed a theory as to what was happening in the field windings. A graphic picture of the distortion of the soft-copper field-winding material then universally used, is shown at the left hand side of Figure 1. The action of repeated starting and stopping cycles is described very clearly in Mr. Noest's paper and does not need to be repeated here.

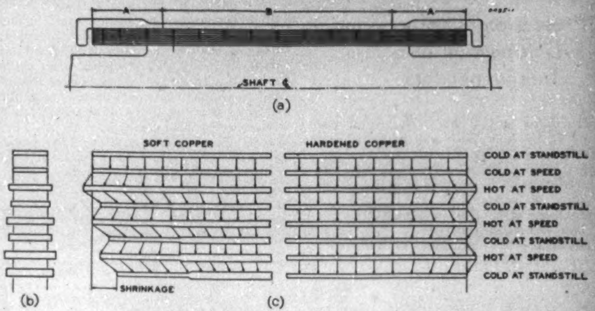

Figure 1 (a). Diagram of field winding in which normal pressure of coil on wedges is about 5,000 pounds per square inch

A—This length partially clamped by centrifugal force

B—This length clamped by centrifugal force acting against wedges

(b). Section of soft copper at *B*

(c). Comparison of soft and hard field copper under repeated load cycles

As a result of the study of this problem in 1934, it was decided to cold-work the field copper about six per cent in area in order to give it elastic properties of a sufficiently high order that temperatures up to 125 degrees centigrade would produce negligible distortion of the field coils. The right side of the diagram shows the performance of a cold-worked copper field winding. The 7,500-kw rotor mentioned was rewound in 1934 with such field coils and has operated very satisfactorily since that time. The rotor of a 183.333-kva 1,800-rpm generator then in process of construction also was equipped with cold-worked copper windings. Shortly after it was installed this unit was run for a six-month period, starting and stopping each night. The loading later was changed to a more uniform one with shutdowns about every three months. Recent examination of this rotor indicated no noticeable shifting of the field windings after eight years' operation.

Experience with over 40 large 3,600-rpm rotors built since 1937 also has been uniformly good which leads us to believe that cold-working of rotor field coils is a distinct forward step in the art of building large turbine generators.

A. W. Rankin (General Electric Company, Schenectady, N. Y.): The paper by Mr. Noest has discussed the important problem of rotor copper deformation and has given in some detail a preheating method which tends to minimize such deformations on those machines on which permanent strains can occur. The company with which this writer is associated has long advocated preheating where applicable and where desirable in order to minimize differential expansion between the winding and the body. Mr. Noest's paper, however, is the first to present the quantitative aspects of rotor preheating and, as such, has presented information which will be helpful in the operation of other machines in similar circumstances.

The winding distortion shown in the end turns in Figures 1A to 1C inclusive of the paper is due to a permanent strain in the rotor copper in the central portion of the rotor body. In this portion of the rotor body the reduced effectiveness of the ventilating ducts causes the radial distribution of temperature to approach that of a machine ventilated only at the gap surface. If Figure 3 of the subject paper were modified accordingly to apply only to the central portion of the rotor where the permanent strain is located, the diminished effect of the ventilating ducts would cause an increase in the temperature of the conductor at the bottom of the slot. Better correspondence than is now shown in Figure 5 then might be available between calculated and test deformations.

The General Electric Company's calculations of the radial distribution of temperature in the rotor slots of the same 160,000-kva generator studied by the author do not agree exactly with the values shown on Figure 3 of the subject paper. Our calculations show a considerably smaller differential between maximum and minimum temperatures than is given by Figure 3.

In considering the distortion of the rotor windings, it should be realized that the axial compressive stress which causes the permanent strain is induced by the frictional forces opposing the temperature elongation. The axial compressive stress increases linearly from its minimum value under the retaining rings to that compressive stress which just balances the potential elongation due to temperature, and the rate at which this compressive stress increases is directly proportional to the coefficient of friction and the centrifugal force. It is accordingly evident that for a given operating temperature, end blocking, and diameter there is a critical length below which no permanent deformation would occur, and that with greater lengths the rate of increase of the permanent strain is far greater than the rate of increase of the machine length.

Since the compressive stress increases at a rate which is directly proportional to the centrifugal force per turn, it is accordingly evident that this rate of increase is much less for an aluminum field winding than for a copper winding because of the lower unit weight of aluminum. In addition, the modulus of elasticity of aluminum is considerably less than that of copper. As a consequence of these facts, the actual compressive stress induced in an aluminum field winding is much less than is implied by a comparison of the thermal coefficients of expansion of aluminum and copper.

The preheating, the characteristics of which were discussed so well by Mr. Noest, is the most positive means of minimizing the winding distortion, and it is advocated wherever desirable and wherever possible. Even on more modern machines using designs and materials which minimize this permanent strain, preheating is inexpensive insurance in those cases in which the service is of extreme severity. Where the operating conditions are not sufficiently severe to result in upsetting of the copper, the preheating described by Mr. Noest will also minimize potential damage to the insulation due to differential movement between the winding and body.

H. D. Braley (Consolidated Edison Company of New York, Inc., New York, N. Y.): The paper by G. A. Juhlien[1] referred to by Mr. Noest presents an admirable analysis of the causes of deformation in rotor windings of turbogenerators and also suggests the use of preheating to minimize these effects. The paper by Mr. Noest presents what we believe to be the first practicable procedure for determination of the correct preheating temperature. This temperature usually can be established at such a value that the stresses in both compression and tension can be maintained within the elastic limits of the field conductors for all ranges of generator loading normally required. The damage to the rotor windings of large costly generators resulting from stresses set up by the combined effects of temperature and centrifugal forces, therefore, may be reduced to a point where it need no longer be a matter of serious concern. The procedure is simple, practical, and may be applied in one form or another by anyone.

The question may be raised as to whether a little distortion more or less is of sufficient importance to cause alarm. In small machines with short rotors and moderate speeds the distortion may take place at such a slow rate that many years may elapse before it becomes appreciable. In very large machines with long rotors the opposite effect may be anticipated. Although we have made no survey of the industry in general—questionnaires being somewhat in disrepute at the present time—the experience of the Consolidated Edison Company of New York may be cited. Serious distortion of field windings have occurred on five generators (two cases on one machine) ranging in size from 20,000 to 200,000 kva and having a total rated capacity of 670,000 kva. All of these troubles have been detected within the past six years. This distortion has caused field winding "grounds" to occur on all of these machines and four of the units have developed short-circuited turns in addition. New rotor windings had been installed in two generators (twice in one of these); one is now at the factory for rewinding; two others have been repaired in the field, one of which is scheduled for rewinding. Machines of three different manufacturers are involved. This would appear to offer sufficient evidence that rotor winding distortion is of some importance and lends emphasis to the value of Mr. Noest's paper.

Though improvements in design have been made and will no doubt continue, it would appear that the physical nature of the problem is such that entire elimination of winding distortion may not reasonably be expected in the near future. One such design might require the use of metals in rotor body and in field winding having the same expansion at their respective operating temperatures and also the same rate of temperature rise. Another method might lie in the use of winding conductors having higher mechanical strength than copper. In the meantime preheating of the rotor windings offers a satisfactory and practical solution.

The testing methods and protective devices developed by the Consolidated Edison Company in connection with the troubles with rotor windings may be of interest and are mentioned briefly:

1. A-c impedance of rotor windings has been found to be indicative of short-circuited turns. Readings, however, must be compared with those taken when it is known that no short circuits exist. This test can be made without removing the rotor from the machine.

2. A field flux survey of the rotor surface with the rotor removed from the machine and the field excited with alternating current affords a positive indication of turn short circuits and also identifies the coil slot in which the fault exists.

3. The exact location of grounds with respect to a point on the surface of the rotor body may be determined by a d-c potentiometer testing method. This test normally requires the rotor to be removed from the machine.

4. A sensitive "ground" detector relay with a sensitivity of 10,000 ohms is connected to the field circuit of operating machines and supervises approximately 95 per cent of the field winding. This relay operates to give an alarm indication only.

5. A protective relay scheme responsive to mechanical vibration amplitude has been developed. It has been used in two instances to protect a generator operating with a single "ground" in the rotor circuit. A moderate increase in vibration operates an alarm, and a further increase automatically trips the generator main switch and field breakers. This equipment has been fully described in the paper entitled, "Vibration Protection for Rotating Machinery, presented before the North Eastern District technical meeting of the Institute by R. L. Webb and C. S. Murray in April 1944.[2]

REFERENCES

1. Deformation of Turboalternator Rotor Windings, Due to Temperature Rise, G. A. Juhlin. *Journal* of the Institution of Electrical Engineers, (London, England), volume 85, 1939, pages 544–52.

2. Vibration Protection for Rotating Machinery, R. L. Webb, C. S. Murray. AIEE Transactions, volume 63, 1944, July section, pages 534–8.

John G. Noest: In regard to Mr. Dissmeyer's question concerning the adequacy of preheating and controlling rotor-winding deformation, the author claims only that preheating permits minimizing of the stresses in the rotor winding. These stresses can not be eliminated completely as long as a cyclic load must be carried. However, since certain types of generators are more subject to winding distortion than others, generators known to be subject to distortion can be favored in the loading, and, if calculations disclose excessive stresses at the extremes of the load cycle which can not be further reduced by the correct choice of preheating temperature, steps can be taken for the reduction of temperature variations by reducing cooling water flow or by throttling cooling air flow at light loads.

Mr. Taylor's point in bringing out the ramifications of the problem of winding distortion which limit the precision of any analytical approach is well taken. The justification of the mathematical treatment lies, not so much in the quantitative results, but in a better understanding of the factors involved.

In regard to the question of ventilation during the preheating period, it has been found by experience that best co-ordination between turbine-starting procedure and preheating results in some cases in preheating with full ventilation and in other cases in starting preheating without ventilation and finishing with ventilation. In none of the five cases where preheating is done on the Consolidated Edison Company system do the preheating operations prolong the normal turbine-starting operations.

With respect to the requirement of complete shutdown, the author believes that the complete release of all accumulated stresses, particularly in the longest rotors, is worth the little extra trouble. It also prevents a tendency on the part of the operators to allow higher turbine speeds than specified.

Vibration Protection for Rotating Machinery

Discussion and authors' closure of paper 44-71 by R. L. Webb and C. S. Murray, presented at the AIEE North Eastern District technical meeting, Boston, Mass., April 19–20, 1944, and published in AIEE TRANSACTIONS, 1944, July section, pages 534–7.

R. P. Crippen (Ebasco International Corporation, New York, N. Y.): The damage that may be caused by a double ground on a 3,600-rpm rotor which short-circuits enough field-winding turns to cause severe magnetic unbalance is illustrated by the failure of a 3,000-kw generator which occurred about five years ago.

The circumstances surrounding this failure are not entirely clear, but, after it was all over, there was a dead ground on the rotor. There was no other disturbance on the system at the time the fault occurred, and there was no evidence of overspeeding on any part of the machine. The governor mechanism tested out perfectly.

The conclusion is that the overspeed device tripped the generator because of extreme vibration from magnetic unbalance due to a double ground of the rotor winding. Among other things the housing of this device was broken.

The extreme vibration caused the inboard generator bearing cap to break. The shaft was sprung, as the rotor was an old type with laminations built up on the shaft. The rotor

sprung over far enough so that the fan blades stripped insulation from the stator coils. The inboard fan failed. This may have occurred first and thus have aggravated the vibration sufficiently to break the bearing cap, but this is supposition. Stator iron was not damaged. Repair of the generator required a complete new rotor, a new stator winding, a new bearing cap, and rebabbitting of bearings, new fans, and other accessories. Very little damage was done to the turbine.

A. W. Rankin (General Electric Company, Schenectady, N. Y.): The paper by Webb and Murray describes a relaying system with which generators have been operated while a single ground existed on the field winding. The efficacy of the relaying equipment itself will not be questioned here, but the practice of operating machines while one ground exists in the field winding does deserve discussion in order that the attending hazards are fully appreciated.

The existence of a single ground in the field winding modifies the distribution of voltage induced in the field winding by armature transients and causes larger voltages to ground to appear than would be present in an ungrounded field. As a consequence, the operation of alternators while one ground exists in the field winding is particularly inviting to the appearance of a second, or double, ground.

A double ground on the field of an alternator partially short-circuits a portion of the field winding, and the resulting unbalanced magnetic forces may cause vibration which can damage the unit severely as described in the introduction of the subject paper. It should be noted, however, that with the occurrence of the second ground, the opening of the field breakers does not immediately eliminate the source of the vibration. The second ground has completed a circuit within the rotor structure, and current will continue to flow in this circuit for an appreciable period (measured in seconds) after the field breakers are opened; in addition, currents will continue to flow in the circuit completed by the discharge resistance across the field breaker. The time duration of the current flow in these circuits is controlled by the open-circuit field time constant modified by the resistances in the grounds and discharge resistor and by the percentage of the field winding which is short-circuited. During the period in which these currents continue to flow, there acts on the rotor the unbalanced force which causes the machine vibration. Under unfavorable conditions these time constants may be about 12 seconds for large turbine generators.

Especially is there a danger present that internal faults will appear in the stator during the period in which the machine is running with one ground. An internal fault in the stator induces transient voltages in the field circuit, and with a single ground already existing in the latter conditions are particularly favorable for the appearance of a second ground. Large induced currents then will be present in the field winding due to the stator fault, and these currents cannot be stopped instantly by opening either field or stator breakers; but they will decay by a time constant which approaches the short-circuit time constant.

The relaying equipment described in the subject paper opens both field and stator breakers upon the incidence of more-than-usual vibration, but it is evident from this discussion that these breakers do not directly or instantly stop the disturbing currents. The hazards attendant upon operation with a single ground naturally are reduced somewhat by the use of this relaying equipment, but it is difficult to state the extent of this reduction. It would be interesting to study any data which the authors might have on the quantitative aspects of this protection; but it is suggested, nevertheless, that operation of machines while one ground exists on the field winding should be undertaken only when conditions make such operation unavoidable and only after careful review of the accompanying hazards.

R. L. Webb and C. S. Murray: The discussion by Mr. Crippen describing an actual case of damage to a generator due to double field-winding grounds is very interesting and is appreciated by the authors. It is probable, had the machine been a larger one, that considerably more damage might have resulted before the rotor had stopped turning. The authors know of at least one other case where similar damages have resulted, except that in that case the stator iron also was damaged severely.

Mr. Rankin's discussion of machine characteristics and how they may affect generator rotor vibration adds materially to the subject under discussion. The authors have considered all of the behaviors which Mr. Rankin brings to our attention and have found that the protection described, as applied to the generator in question, does in their opinion provide satisfactory protection under the most adverse conditions which may be encountered.

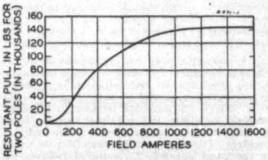

Figure 1. Resultant attraction between two poles and stator iron with varying field current

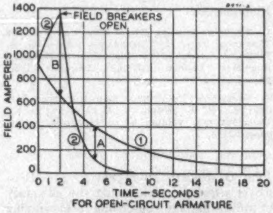

Figure 2. Rate of field current change in windings measured from time second ground occurs

1—The two short-circuited poles
2—The remaining two poles
A—27,000-pound pull, 12-mil vibration
B—72,000-pound pull, 40-mil vibration

Calculations of field rotor vibration due to double field-winding grounds require that consideration be given to the following items:

1. The initial flux condition.
2. The type of field-winding failure and how it affects the flux in all of the magnetic poles.
3. The rate at which field current and magnetic flux change for each field pole for both open-circuit and short-circuit armature conditions.
4. The relation between unbalanced flux densities and unbalanced magnetic attraction.
5. Calculation of shaft, bearing, and mounting steel deflections in both horizontal and vertical planes to follow these changes in magnetic forces.

A practical calculation of a hypothetical or actual case to obtain the results for a case of double field-winding grounds necessarily must entail some simplifying assumptions. The field-winding ground on the machine with which this paper is concerned is located at about 30 per cent of the winding from the negative slip ring. This places the ground in the first coil of the second pole winding from the negative ring. To simplify the solution it was assumed that this ground is located at 25 per cent of the field winding, which places it between the first and second pole windings from the negative ring. In order to obtain a very severe case, it was assumed that a second ground occurs between the third and fourth pole windings, thereby short-circuiting one half of the field winding and leaving the other half connected to the supply, which is assumed to be a constant voltage source.

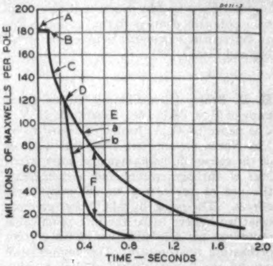

Figure 3. Field fluctuations for three-phase terminal short circuits

A—Armature short circuit (three-phase terminal short circuit)
B—Subtransient effect ended after about 0.1 second
C—Field decays for 0.15 second to follow standard decrement curve
D—Breakers open by generator differential relays at 0.25 per second
E—Decay of flux after field breaker opens
a. Two poles short-circuited
b. Two poles with winding on discharge resistor
Maximum flux difference between poles is 62×10^6 maxwells at 0.5 second
F—About 0.007-inch vertical vibration or 0.014-inch horizontal vibration would be expected to occur because of the 25,000-pound differential pull on one side of the rotor

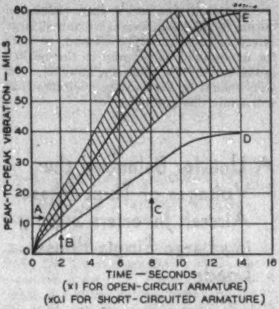

Figure 4. Rate of change in vibration amplitudes following second ground in field winding (two pole windings short-circuited)

A—Vibration relay trip setting

B—Breakers open (2.0 seconds), armature open-circuited

C—Breakers open (0.8 second), armature short-circuited

D—Calculated vertical vibration

E—Estimated horizontal vibration and probable range of variation

These assumptions assist the calculation, since they permit considering the magnetic forces being exerted along two resultants which are 180 mechanical degrees apart—one of which is controlled by the current in the pole windings which are not short-circuited, and the other is controlled by the pole windings which are short-circuited.

The curve of Figure 1 was obtained to show the magnetic attraction versus field current for the resultant of the magnetic forces of two magnetic poles. The curve takes into account the effects of saturation.

The curves of Figure 2 show the rate of change of field current in the windings of the two short-circuited poles and in the windings of the poles which are not short-circuited measured from the time the second ground appears. The current in the short-circuited portion of the winding begins to decay at a slow rate immediately following the occurrence of the second ground. The current in the other two pole windings increases initially and then decreases at a more rapid rate, when the field breakers are opened and the winding is connected to the field-discharge resistor. The data presented on this curve are for open-circuit armature conditions.

If the occurrence of a stator winding short circuit should, as pointed out by Mr. Rankin, be the cause of the higher voltages between turns of the field winding, which in turn cause the second field-winding ground to appear, the behavior would be somewhat different. Though the field current in the short-circuited and unshort-circuited portions of the windings would be widely different, the field flux across the air gap would not be expected to increase. Since the armature short circuit is assumed to be on the generator terminals and since some time is required for vibration to develop, the generator differential relays would be likely to operate before the vibration-protection relays. The curves of Figure 3 show how the magnetic flux per field pole would vary with time following the occurrence of a three-phase short circuit at the generator terminals. When the field circuit breakers are opened by differential relay operation at about 0.25 second, the magnetic flux of the poles with short-circuited windings will decay relatively slowly, whereas the flux in the other two poles will be forced to decay rapidly, since the windings of those poles are connected to the field-discharge resistors. For this condition, the maximum machine vibration which would be expected is relatively small as indicated.

The curves of Figure 4 are approximate only, since they involve the complications associated with determining the deflections which may occur in the generator foundation steel. However, the curves do indicate the approximate maximum vibrations which may be expected and how they vary with time for both open-circuit-armature and short-circuit-armature conditions. The generator circuit breakers are opened at about 0.8 second when the armature is short-circuited by action of the vibration-protection relays. This permits a maximum vibration of 50 to 60 mils at that moment. However, the vibration following the opening of the circuit breakers would, for this case, be expected to decrease, since the field poles which are strong at 0.8 second would have their windings connected to the discharge resistors, and the field flux in them would decay at a more rapid rate than the field flux in the other two poles whose windings are short-circuited.

The precautions which Mr. Rankin has called to our attention are important and should be considered for each machine to which vibration protection is applied. It is believed by the authors that the vibration protection applied to the machine with which the paper is concerned does provide adequate protection against serious machine damage due to vibration caused by double field-winding grounds.

Behavior Factors of Rectifier-Driven D-C Motors

Discussion and author's closure of paper 44-86 by Victor Siegfried, presented at the AIEE North Eastern District technical meeting, Boston, Mass., April 19–20, 1944, and published in AIEE TRANSACTIONS, 1944, July section, pages 530–2.

B. J. Dalton (General Electric Company, Schenectady, N. Y.): This paper has presented a rather pessimistic picture of the operation of d-c motors on rectifiers. That this picture is not actually as pessimistic as might be assumed is proved by the operating performance of hundreds of drives of this type now in operation.

The inductance in the motor itself provides a great deal of filtering and aids in the smoothing of the current wave shape. Because of the inherent characteristics of a motor operating on a rectifier, the form factor is worse at light loads than it is at full load. Therefore the picture will look much worse, if considered at light loads, than it will at full loads. However, the total amount of heating in the motor is least at these light loads and is greatest at full load where the current is generally continuous.

The form factor of the armature current is dependent, for a given horsepower rating, upon the speed and the frame size of the motor. A low-speed large-frame-size motor, because of its inherent inductance, has less heating as contrasted to normal than a high-speed small-frame-size motor.

Single-tube rectifiers, because they present a very discontinuous voltage, practically are limited in application to very small motors. Some tests have indicated that the heating losses of a motor on a single-tube rectifier may be about three times normal.

Motor performance on two-tube rectifiers is not as bad as one might expect. The heating losses will be very roughly between a little over normal to about twice normal.

A rectifier consisting of three or four tubes will provide a voltage which has only a small amount of ripple, and the current in the motor will be filtered to such an extent that the increased losses are often negligible. Economics alone usually will determine whether it is more feasible to use a small number of tubes and a large motor, or a large number of tubes and a small motor; or whether or not an external reactor may be added in the armature circuit to reduce the motor frame size.

This paper mentions the harmful effects of the voltage peaks present. The peak voltage applied to the motor is dependent on the type of rectifier used. For a two-tube rectifier on a 230-volt motor the peak voltage will be approximately 440 volts, which should not present an undue stress on the motor insulation.

In order to insure successful operation of motors on rectifiers these factors should be considered. However, with proper motor design and co-ordination of motor and control these problems do not present too difficult a solution.

Victor Siegfried: Mr. Dalton's discussion is very welcome, inasmuch as it appears to confirm the convictions expressed in the paper. Far from being pessimistic, however, these conclusions are intended to be realistic and to call attention to factors which, if neglected, may lead to improper engineering of rectifier drives, and hence hinder real progress. That such factors are being considered in the hundreds of drives now in operation is ample proof of the need for this consideration, although previous literature and experience with commercially available units did not always show it had been given. In fact definite lack of it has been observed in a few installations where the disagreeable factors outweighed the optimistic features in the customers' minds. The reputation of this attractive system, unfortunately, is harmed more by these few instances than it is enhanced by many other more favorable applications.

With regard to the use of the inductance of the motor for smoothing, the least filtering is required if the full-load setting of the rectifier is the full-on condition. There is a practical limitation, then, which calls for some remanent latitude in the controls, and thus requires a compromise at something less than this. We are thus back at the original situation where the heating by the pulsating current is excessive over the *d-c* ammeter value. In applications where duty cycles are to be considered, the true heating value is of utmost importance, as we cannot on the one hand say to disregard these differences, and then on the other figure the duty cycle with the lower value.

It is gratifying to hear that the heating ratios are three times normal for single-tube rectifiers and up to twice normal for two-tube rectifiers, as these check the ranges suggested in the paper from the theoretical analysis. As Mr. Dalton points out, the combination of multiphase rectifiers and realization of filtering properties from inductance, either in or external to the armature, may reduce this increase in loss to negligible proportions. Is not this ample justification within limits of economics for standardization on using the largest permissible number of tubes? That was, in fact, the most important of the conclusions in the paper, because the multiphase rectifier avoids the necessary derating of the motor and introduces other benefits which are more difficult to equate in the economics of the situation.

I would urge caution in the philosophy of inserting inductance to secure better wave form. These 230-volt systems find the thyratrons already operating at the ragged edge of their 1,000-volt inverse voltage limit, and inductance serves to aggravate the occurrence of additional voltages from sudden commutation of currents. It is my belief that from the voltage standpoint the less inductance the better. A compromise must be reached, of course, with the desire to smooth the current waves.

As to peak voltages, there are apparently some conflicting ideas on possible values. With a quick reversal of the motor, for example, voltage peaks of more than double the 440-volt theoretical figure have been observed by oscilloscope, and commutators have been flashed over by these voltages. The possibility suggests itself of stray Ldi/dt voltages caused by sudden current changes in the armature beyond the scope of the suggested 440-volt peak. Once again the avoidance of high-inductance armatures is seen to be desirable.

To reiterate, the relative importance of the various factors outlined in the paper must be left to the manufacturers' or users' discretion. Their inclusion in designs, however, cannot hinder real and permanent progress in the art of rectifier drives.

Aids to Quality Control in the Manufacture of Aircraft Generators

Discussion of paper 44-83 by F. M. Potter, presented at the AIEE North Eastern District technical meeting, Boston, Mass., April 19–20, 1944, and published in AIEE TRANSACTIONS, 1944, July section, pages 525–9.

R. R. Mille (Westinghouse Electric and Manufacturing Company, Lima, Ohio): Mr. Potter has described many interesting tests which should be very helpful in maintaining quality of aircraft generators. However, he does not state which ones are used as regular production tests or to what degree they are used.

If a large number of defects are likely to be found, tests in the early stages of manufacture are necessary. However, if by the use of proper manufacturing methods, quality is built into the parts in each step, there will be less rejects and no need for so many detailed tests. With this method only a few tests to assure satisfactory over-all performance will be required, and a better product will result.

Complete stators need be given only a comparison test, reading the watts input on alternating current to check electrical and magnetic qualities. Armatures need be given only a special high-voltage high-frequency test which checks insulation, short-circuits, connections, and reversed coils where the armature is part of a resonant tuned circuit. This test is unique in that it is simple to perform yet will detect faults which ordinary tests cannot find. The final over-all test should be the performance test on the complete generator. To obtain a thorough check this test should be made at full-load temperature. This is especially important on aircraft generators, since they are operated at higher temperatures than ordinary machines. Movement of commutator bars and of windings and poor connections are more likely to occur at these high temperatures.

Mr. Potter has not mentioned any special tests to check the quality of the carbon brushes used on aircraft generators. Brushes are a vital part of any d-c machine, and several special tests have been very helpful, not only in maintaining quality standards, but also in leading to material improvements in manufacture of the brushes. The usual brush life test does not give any indication of the results which may be expected from actual service on an airplane engine. The effect of vibration in particular must be taken into account in any test of the suitability of brushes for aircraft-generator service. The use of an impact tester to check the impact resistance of brushes has been very valuable in comparing brush material and also in determining whether the brush has been weakened by attachment of the shunt connections.

The problem of shunt attachment on brushes, which must operate under conditions of high temperature and excessive vibration, is one that has given brush manufacturers a great deal of trouble. An attachment which is too tight may damage the brush so that it will fracture, and any looseness in the attachment will result in high resistance and rapid failure of the joint. The endurance of shunt connections has been tested by a mechanical arrangement consisting of an eccentric commutator provided with several steps which drop the brush about $^3/_{64}$ of an inch. This is admittedly a very severe accelerated test, but it will show poorly riveted shunt attachments, solder in the shunt strands, and sharp edges on the terminals. Any mechanical weakness in the brush will result in failure in a matter of hours on this tester. This is not a quantitative test, but it has been very useful for quick comparisons of various lots of brushes, and its use has helped maintain the quality of the brushes.

On aircraft generators where maximum output for minimum weight is required, large safety factors cannot be allowed, so tests for maintaining uniformity in the quality are essential. For economy, however, such tests should be kept to the minimum that will assure this quality.

A Unit for Balancing the Voltages of Three-Phase Aircraft Inverters Supplying Large Single-Phase Loads

Discussion of paper 44-72 by Sydney Wimpie, presented at the AIEE North Eastern District technical meeting, Boston, Mass., April 19–20, 1944, and published in AIEE TRANSACTIONS, 1944, July section, pages 532–3.

Alexander H. Wing, Jr. (Harvard University, Cambridge, Mass.): The device which Mr. Wimpie describes is very clever indeed. There is one point in the analysis however, which did not at first seem clear. Following equation 11, Mr. Wimpie states, "It can further be seen that if the internal impedance of the power supply is small compared to the impedance of the load

$$I_{C1}/I_{C2}=E_{BP}/E_{PA} \qquad (12)\text{"}$$

The operation of the tap on the inductor and the division of capacitor current might be explained as follows.

The inductor may be regarded as combining the effects of an untapped coil across phases *A* and *B*, together with a tapped ideal autotransformer as shown in Figure 1. As is clearly shown in Figure 7 of the paper, the capacitor current I_C is such that the load current plus the capacitor current equals $I\phi_1$ as determined by equation 8 of the paper. This establishes I_C on Figure 7. The voltage vector E_C, Figure 7 is drawn perpendicular to I_C, fixing point *P* and the location of the tap on the transformer. Since the transformer is ideal, the volt-amperes in each winding must be equal, so that

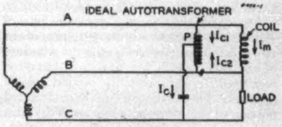

Figure 1

$$E_{AP}I_{C1}=E_{BP}I_{C2}$$

Thus it is not necessary to assume a zero-impedance source to establish the division of the capacitor current.

As a matter of fact, Mr. Wimpie's device works just as well as a balancer, even if the generator has considerable balanced internal impedance. For example, if the addition of other balanced loads should cause the voltage to decrease considerably (indicating considerable internal impedance), the vector diagram of Figure 7 simply would shrink in proportion, and the balance would not be disturbed.

Mr. Wimpie is to be congratulated on the development of this piece of equipment which is remarkable in its simplicity and so effective and useful in our war effort.

A-C and D-C Short-Circuit Tests on Aircraft Cable

Discussion and authors' closure of paper 44-81 by J. C. Cunningham and W. M. Davidson, presented at the AIEE North Eastern District technical meeting, Boston, Mass., April 19-20, 1944, and published in AIEE TRANSACTIONS, 1944, pages 961-9.

J. C. Cunningham (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): One of the most effective methods of improving the fault-clearing ability of an aircraft distribution system is to replace heavy cables by two or more smaller cables in parallel. By this means the probable thermal capacity of faults is reduced without a corresponding reduction in short-circuit current. The result is a reduction in fault time, damage, and fire hazard. The increased number of circuits further increases reliability by reducing the probability of losing all circuits.

In the design of an aircraft electric system one should consider the need for sustained short-circuit current in order to assure clearing of heavy faults. This is provided in present low-voltage d-c systems by the use of batteries, although their ability to deliver current diminishes rapidly at low temperature. Unfortunately shunt-excited generators deliver less sustained current to a very heavy fault than to one of higher resistance. If the initial transient fault current is not sufficiently high to assure clearing of faults in a few tenths of a second, it may be advisable to provide sustained current by means of separately excited generators, by batteries or otherwise. In a-c systems the use of integral or coupled exciters has the additional advantage of improving the stability of the system.

A. C. Monteith (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The results of the tests reported in this paper are of interest, since they furnish good comparative results of burning clear or the self-clearing characteristics of faults as a function of voltage and current for alternating and direct current under different altitude conditions.

In studying the results of these tests there is always a question of how closely they represent all possible combinations of faults under service conditions. To attempt positively to test all conditions would result in a prohibitive number of tests. The results of the series of tests presented in the paper do, however, give a good comparison of the fault-clearing characteristics for the stated conditions and show how the fault-clearing characteristics are affected by the varying conditions. There is reasonable expectancy that they are indicative of what will happen for all possible types of faults.

In the case of making the original designs for secondary-network systems installed in metropolitan areas a similar set of tests were made under what appeared to be the most severe conditions. These likewise showed comparative results which have been substantiated by 20 years of service experience.

R. J. Lusk (captain) and **M. M. Moorhead** (lieutenant; Army Air Forces Matériel Command, Wright Field, Dayton, Ohio): Mr. Cunningham and Mr. Davidson have conducted an extremely well-reported series of tests covering the phenomena of clearing of a-c and d-c short circuits of aircraft cable. No disagreement can be made with their conclusions regarding fault phenomena with respect to current levels, air density, voltage, and thermal capacity.

However, application of these conclusions to military aircraft should not be made without serious consideration of several pertinent factors. The report was concluded with statements which, in effect, say: Faults will occur and therefore protection should be used. This conclusion would be more acceptable, if it were modified to state: Based on these tests serious faults may occur and protection therefore is justifiable, provided further considerations do not alter the picture.

Because of errors in certain basic assumptions made during the course of the tests, it is believed that the results are unduly pessimistic and cannot be applied satisfactorily to military aircraft.

Tests 25, 26, and 27 were made to determine the effect of three types of severed wire ends upon clearing phenomena. The conditions represented by test 25 were considered "too favorable" to be representative of many faults, in spite of the fact that tests conducted by the Army Air Forces Matériel Command and reports from theaters of operation indicate that the conditions represented by test 25 are actually the conditions encountered. This is an error in one of the basic assumptions and affects the practical application of the test results to military aircraft.

It was stated that in order to obtain fault contact it was usually necessary to clean the aluminum alloy fuselage surface with emery paper. This represents a wide deviation from conditions likely to be encountered in service. Military aircraft are subject to wide extremes of atmospheric conditions with the result that the aluminum alloy surface in all probability would resemble the original condition of the fuselage used in the tests. In addition present trends toward anodic treatment of aluminum surfaces for aircraft will provide additional insulating properties.

Deviation of test conditions from actual service conditions make the application of test results to military aircraft questionable. A practical application of even perfect test results to military aircraft cannot be made without proper consideration of other factors involved in the installation of protective devices.

Economics must be considered. That is, the cost of protection must be balanced against the value of the benefit derived. Cost in this instance does not refer to dollars and cents but to the much more important factors of increased maintenance and increased weight.

The function of a bombardment air force is to deliver bombs to a target. The effectiveness of a bombardment air force is measured by the weight of bombs it delivers to its targets in a given period of time. The weight of bombs delivered can be decreased by increasing the weight of the airplane thus decreasing the bomb load or by reducing the availability of airplanes for missions by increasing necessary maintenance operations. The addition of devices to offer protection against cable faults does both—increases maintenance and decreases bomb load.

Maintenance is a serious bottleneck in combat operation and seriously limits the striking power of a combat air force. Available schemes for protection against cable faults require considerable maintenance without furnishing completely infallible protection. These schemes probably would require a complete inspection of the electric system following each combat operation. This inspection is classed as preventative maintenance, but reports from combat areas emphasize lack of time for preventative maintenance thus raising a serious objection to these schemes.

Weight is all-important. A pound of additional weight added to a bombardment airplane decreases the possible bomb load by one pound plus the weight of fuel required to carry the added pound from the target back to the home base. A hundred additional pounds added to each bomber in a 1,000 airplane raid would decrease the possible bomb load by considerably more than 100,000 pounds. This additional weight, in effect, would neutralize more than five fully loaded heavy bombers for each such raid.

Because of the maintenance and weight factors it can be seen that a real need for protection against cable faults must be established before the incorporation of protection schemes in military aircraft can be undertaken.

Careful consideration must be given to the probability of occurrence in service of the various fault phenomena demonstrated in the laboratory and to the relative seriousness of these events with regard to the performance of the military mission and safe return to base.

From analysis of combat reports and from results of actual gunfire tests it can be concluded that the occurrence of line-to-line faults and high-thermal-capacity faults, while admittedly possible, is of such a low order of probability that it fails to justify the use of extravagant protection measures. The great majority of gunfire faults apparently are of the low-thermal capacity, light-contact type, which, all indications show, burn clear with a minimum of danger.

An analysis of the higher-voltage system proposed for use in military aircraft will show that the majority of cable faults which can be expected to occur will meet the conditions set up by Mr. Cunningham and Mr. Davidson for rapid clearing, that is, the available current level is high enough for quick clearing of light-contact faults on the smaller wire sizes utilized in these high-voltage systems.

Aircraft-electric system reliability depends not only on the rapid clearing of cable faults but also on protection against occurrence of open circuits and against faults occurring in electrical equipment and at cable-junction points. Service experience has shown these items to be worthy of serious consideration, not only because they may be caused by gunfire, but also because they may occur during maintenance or as a result of normal wear and tear.

Open circuits may cause loss of power to important electrical loads such as gun turrets. The use of multiple-channel busses and distribution circuits greatly reduces the danger of power interruption to important loads resulting from open circuits due to gunfire or other causes.

Protection against faults at cable-junction points is difficult to attain by the use of current-interrupting devices without addition of considerable weight and complication, however, adequate protection may be obtained by preventing occurrence of such faults. The probability of occurrence of cable-junction faults may be reduced satisfactorily by proper isolation of junction locations from the grounded structure of the airplane.

Investigation of circuit faults other than those due to gunfire indicates that the great majority of these faults could be averted by proper and careful installation.

Electrical equipment is subject to faults due to gunfire, to normal wear and tear, and to misuse. These faults usually are not of the self-clearing type and it is usually difficult to clear these faults quickly. Thermally actuated devices should be used to minimize effect of equipment faults on the electric system. Circuit devices should act to remove faulty equipment from the line, when the circuit is dangerously overloaded. Where it is deemed advisable to prevent fault current flow from damaging equipment, protective devices should be incorporated in the unit.

J. C. Cunningham and W. M. Davidson: The authors agree with Captain Lusk and Lieutenant Moorhead that the tests do not represent typical gunfire faults and that the test results do not show that sustained faults are likely to occur. However, a careful review of the purposes of the tests and the primary conclusions stated in the synopsis will reveal that we were trying to determine why faults in present systems do clear, not whether or not they would clear; and whether higher-voltage systems would be less satisfactory in this respect. Our conclusions attributed satisfactory clearing to cable fraying and adequate current level and reported no unfavorable effect of the higher voltages contemplated.

In order to make a satisfactory comparison between present systems and proposed systems of different voltage, fault current level, and frequency it is essential to use a type of fault which can be reproduced consistently and still represents actual conditions as nearly as possible. The type of fault which Captain Lusk and Lieutenant Moorhead recommend for "practical application of the test results" would vary so much in a series of tests that the effect of voltage, current, and frequency would be obscured.

Similarly, their objection to cleaning the aluminum surface with emery paper to obtain contact would be tenable, only if we were attempting to show whether faults would occur, rather than how clearing is affected by circuit conditions.

A third dissimilarity between test conditions and actual service conditions, which is perhaps equally as important as frayed strands and a poorly conducting surface, is the test procedure where power was applied after the cables were placed in short-circuit position. While this procedure is justified in order to obtain identical fault conditions for comparative tests, a short circuit in actual service may be considerably different because the cable is energized and in motion before the short circuit occurs. This was mentioned in conclusion 7.

From the foregoing discussion it may be concluded safely that the type of fault represented by the test conditions is more severe than those likely to occur in service. It is impossible to say, either on the basis of these tests or on the basis of observations made thus far under service conditions, how much more severe.

With regard to aircraft electric systems it is interesting to note that in the 400-cycle systems now under development the ratio of available fault current to load current and to cable size is much more favorable than in present d-c systems, and, therefore, on the basis of these tests faults should burn clear more rapidly. At the time this paper was written it appeared very likely that protective devices would be required in proposed 400-cycle systems to avoid loss of synchronism or motor stalling. Although stability tests have not yet been made, it now appears that the few statements in this respect were premature.

Conditions Controlling the Economic Selection of Prime Movers

Discussion and author's closure of paper 44-107 by Bernhardt G. A. Skrotzki, presented at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944, and published in AIEE TRANSACTIONS, 1944, volume 63, 1944, pages 1099–1108.

W. E. Caldwell (nonmember; Consolidated Edison Company of New York, Inc.; New York, N. Y.) The paper is a noteworthy contribution and effectively portrays cause and effect in evaluation procedure. The analytical treatment is rational and affords a simple approach in determining the justifiable investment factor for comparing alternate schemes of development. The importance of predictions which form the basis of evaluation is made obvious in the paper. Since evaluation studies precede initial operation of the equipment selected by about two years, it is necessary to forecast the plant operating history from a period long before it exists all the way to obsolescence.

Predicting load growth, load factor, interest rates, system interconnections and other influences in choice of equipment is speculative but does not detract from the soundness of the principles of economic justification. Among the influences which have altered past predictions in metropolitan systems might be mentioned: interconnection with other systems or with hydro systems; topping of older plants with high-pressure units; the use of exhaust steam from high-pressure units for district heating. Topping of low-pressure plants may increase the useful life of the topped low-pressure units from an expectancy of 20 years to 50 years or more, with corresponding increase in load factor. This results in reduced load factor for the untopped units on the system. District heating with exhaust steam from high-pressure units reduces load factor on older plants in the same manner as dump hydro power.

Past experience indicates that properly maintained power-generating equipment never completely wears out. Boilers, turbogenerators, and other plant equipment have been retired from service due to obsolescence, after 30 years of operation, which were practically equal to new, so far as the intended service was concerned. Except for obsolescence, this apparatus might have enjoyed a century of useful life. Future apparatus may not possess this longevity since present tendencies are to design with less margin.

Since the cost of plant capacity diminishes with increasing size of the individual units and the unit labor cost for operation also diminishes, the tendency is to install larger sized units than strict evaluation procedure dictates. Furthermore, prudent management is inclined to discount savings contemplated in the remote future. The present worth of savings expected 20 years or more from the date of purchase is not tempting enough to outweigh the uncertainty of the predictions. For this reason, the justifiable-investment factor is usually held to a prudent limit which is in keeping with the changing times.

Experience often dictates sacrifice in efficiency in the interest of simplicity and reliability. With a background of past experience and some indication of future possibilities, the range of probable savings may be readily appraised. To this end, the author's graphical treatment of the factors involved is a welcome contribution.

T. G. LeClair (Commonwealth Edison Company, Chicago, Ill.): The paper by Mr. Skrotzki including the development of the formula for economic selection of prime movers is an important contribution to the central-station engineering field. A variety of different methods have been used by electric power-companies in selecting the most economical design of turbine-generator units for particular conditions. Most of these methods have not been published for the benefit of other engineers who may be interested in making such an economic study. The methods given by the author are quite complete and should be very helpful to engineers faced with this problem.

This problem is of particular importance to utilities as distinguished from the manufacturing industry. In the utility industry the invested capital may be five or more times the annual gross income. This is the reverse of a manufacturing industry where physical plant may be only a fraction of annual business. Therefore, it is essential to the success of a utility enterprise that all new plant equipment be judiciously selected. I should like to stress the statement made by the author after his equation 15 that "The investment will not be undertaken unless its value is less than that determined by the equation." This is particularly important as it is difficult to weigh the many factors which may enter into the determination of the useful life of the equipment, especially those mentioned by the author on the future advancement of the art and the estimated life of the enterprise.

The experience of the railroads is a suitable illustration. Many years ago the railroads developed passenger equipment which had such stamina and wearing power that it was expected to remain in service for as long as 50 years. Not only was it built for this purpose but the rate of depreciation used in determining the annual charges was such that equipment must be used for this longer period in order for the enterprise to be financially successful. When styles changed and automobiles and busses were made more attractive and comfortable than railroad cars, the railroads could not afford to scrap the old equipment in meeting this competition. It was found that the newer equipment, even though lighter, would require additional carrying charges and, therefore, old equipment was kept in service. The old equipment was naturally less popular with customers than more modern design and this factor, in my opinion, had much to do with the inroads made by the automobile and motorbus into the railroad passenger business. The competing form of transportation had no existing equipment to be used up and had no inhibitions against the introduction of novelties which were an improvement.

Fortunately for the railroads, the war period has increased travel and decreased the availability of the private automobile. Old equipment now is being regularly used which admittedly had little remaining value.

The purpose of the preceding statements is merely to illustrate the fact that it is extremely difficult to predict the useful life of any equipment which is to be purchased. It, therefore, seems necessary in using the equations of the nature presented in the paper to put much greater value on the present than the future. In other words, it seems quite important that unless savings are to be obtained in the very near future, or the investment to be paid for in a short time, ordinarily it is not good judgment to undertake the new investment.

In many specialized manufacturing plants, new tools and equipment are purchased only if they can be paid for out of increased efficiency in six months to a year. In the utility industry, where equipment may have a life of 15 to 40 years or more, it is my opinion that it is seldom good practice to install new plant solely for operating savings unless the net savings over and above operating expenses will add up to the total of the new investment in a period of from five to seven years. Such a discount on the expected savings is necessary to insure that new capital is prudently invested in the light of many contingencies which may occur. In the selection of prime movers, needed for additional generating capacity, the foregoing statements apply to the cost of improvements for greater operating economy.

In the last analysis, any formula supplies figures only as a basis for sound judgment when weighing possible unexpected changes in the industry, possible new sources of power, and other factors beyond our control.

Bernhardt G. A. Skrotzki: The discussions of Mr. Caldwell and Mr. LeClair emphasize the haze of uncertainties that the power-system designer is forced to work in when proportioning and evaluating the component parts of a system. Though these uncertainties exist, every precaution should be exercised to insure that all factors have been critically examined.

Usually the justifiable-investment factor is discounted to guard against overoptimistic assumptions and estimates. An important distinction in the case of generating equipment must be made as to its annual production rate during life. A substantially uniform production rate will have a higher justifiable-investment factor for evaluation purposes than for the case of decreasing production rate arising from addition of units to meet load growth. In an endeavor to avoid the considerable work required to correlate the justifiable-investment factor and the load-growth estimate, the temptation may arise to apply arbitrarily a large discounting factor to the justifiable-investment factor applicable for the condition of uniform annual production rate. The disadvantage of such a procedure lies in the addition of an avoidable uncertainty of major importance to a host of unavoidable uncertainties.

Though the treatment in the paper is mainly theoretical, it is the hope of the author that it contributes to the field of practical economic evaluation in the same manner that the highly theoretical subject of thermodynamics contributes to the field of practical power-station design.

The Summation of Load Curves

Discussion and author's closure of paper 44-98 by R. F. Hamilton, presented at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944, and published in AIEE TRANSACTIONS, 1944, October section, pages 729–35.

Percy H. Thomas (Federal Power Commission, Washington, D. C.): This paper and an earlier paper by Mr. Hamilton provide an ingenious method of estimating the effect of load factor and peaks in the summation load curve of a group of consuming appliances. This plotting of summation load curves for the information of load dispatchers may be gotten at in other ways, to advantage in some cases.

For example each unit load curve may be divided into a series of portions distinguished by the time period during which the energy is consumed. At the bottom of the curve, for example, is set off a portion or block of power which includes all the energy under the load curve delivered over a period of at least 20 hours. This block would be set off from the rest of the load curve by a horizontal line across the curve at the 20-hour level. A generator carrying this load would operate 24 hours but would carry full load for only 20 hours.

The next block might be chosen as the power for which delivery requires at least 16 hours but not over 20 hours. This would be set off by a horizontal line across the load curve at the 16-hour level. The generator carrying this block would operate at full load for 16 hours only and would be free after 20 hours. Next might be a block between 8 and 16 hours. This would leave the peaks constituting the zero-to-eight-hour portion the sum of the four portions covering the whole load curve. Five or more blocks might be used. For any local load each of these blocks will have a peak capacity value and an energy value. The sum of all the blocks corresponding to the first block of the several component loads in a system would give a larger block of energy having the same time period as all the first block components, a peak capacity equal to the capacity chosen for the first block peaks, and a total energy equal to the sum of all the first block energies. Similarly with the summation of all the local second-block peaks and their energies into a second block with a total capacity peak and a total energy. The treatment of the other blocks would be the same, and all taken together would give an approximate group load curve for the whole load.

It is clear that the various blocks of the same order, for example the eight-hour blocks, might not cover exactly the same eight-hour period, and the summation block might in fact extend over 9 or 10 hours at the main generators. This fact offers no difficulty, for naturally these load-carrying generators will be operating as spinning reserve before and after their eight-hour load period and thus could handle readily any fringe of energy. The total capacity value and the total energy remain unchanged.

The peaks constitute a special block, since their time of occurrence and energy values are varied. A special diversity study may be made of them, or the peak load value may be estimated from the energy of the peak block, or the peak capacity may be merged with the spinning reserves. In each case the most appropriate method may be chosen.

This will be found a very simple and effective method of integrating a large number of scattered loads. At the same time it gives the necessary data for determining the loading and required capacity for the transmission lines concerned.

It will not be necessary to have a load curve for each component load, because an appropriate set of load-curve blocks for any consumer, each with its own block capacity and energy values, can be estimated from a set of typical load curves.

By this block method all numerical operations are mere additions and subtractions, after the initial setting up of the problem. This method actually has been used but will not be considered further in this discussion.

W. J. Lynott, Jr. (French Supply Council, Washington, D. C.): A perusal of Mr. Hamilton's paper which enlarges upon "Synthetic or Equivalent Load Curves" by the same author[1] indicates a novel approach to multiple-load forecasting.

For almost any conditions of load-curve summation the equations were found to possess a degree of accuracy consistent with plotted values. However, it might be stated in fairness to the author that equation 12 must be used with discretion in reference to the time displacement of the peak loads. A wide time variation in the occurrence of peaks would prevent the accurate use of this particular formula.

The peaks of range and water-heater demands actually measured on a large utility system and their resultant diversified loads were found to agree favorably with the values calculated by Mr. Hamilton's equations. Anticipated postwar developments of the electric industry will place many new loads on utility systems varying in magnitude, load factors, and diversities.

To this end it would seem advisable to encourage the conscientious study of probable loads and their characteristics well in advance. Mr. Hamilton's paper serves a useful purpose in provoking some thought in this direction.

REFERENCE

1. SYNTHETIC OR EQUIVALENT LOAD CURVES, R. F. Hamilton. AIEE TRANSACTIONS, volume 61, 1942, pages 369–81.

Constantine Bary (Philadelphia Electric Company, Philadelphia, Pa.): I have had several discussions with Mr. Hamilton on his paper, and I pointed out to him the necessity for clarifying and extensively qualifying the limitations of certain of the formulas and statements contained in the paper to obviate the possibility of erroneous results in their applications to actual conditions. This discussion constitutes my comments on certain selected phases of the paper.

The paper contains no adequate demonstration of the accuracy that will be obtained from an application of the more important empirical mathematical relationship developed (such as equations 12 and 14) to actual conditions encountered in practice. Examples contained in the appendix are helpful demonstrations of the arithmetic involved in the use of certain of the formulas shown in the paper but, except for one, do not demonstrate their validity for practical applications.

The use in the paper of the well-known load-duration curves for obtaining the sum of individual loads at any time of day has no theoretical basis of fact. It may produce results with sufficient engineering accuracy for certain limited conditions. But its unqualified use for generalized conditions as indicated in the paper is not warranted by the fundamental characteristics of these curves. Basically, a load-duration curve depicts the relationship between load and its relative duration during a period under consideration. It gives no indication of the time occurrence of the load. Its use for determining the sum of loads at specified times may apply with some degree of accuracy in a limited manner to load curves which have symmetrical shapes about their peak and which in their contours approach the shapes of curves obtained from a probability function; but it is obvious that for the general case of load-curve shapes encountered in practice, which ordinarily are neither symmetrical about their peaks nor approach in shape a probability function over the entire day, the load-duration curves cannot serve as a reliable means for determining the sum of the loads of various shapes at all times of day.

Equation 12 in the paper is based upon the general characteristics of a load-duration curve. To test its validity in practice and following the examples of calculations shown in the appendix to the paper, I had it applied to actual data of seven groups, each consisting of ten consumer load curves selected at random from available load curves of commercial and industrial consumers of the Philadelphia Electric Company for a day in December 1942. The results derived by equation 12 then were compared with the actual additions of the loads at the selected hours of 4 a.m., 10 a.m., 6 p.m., and 10 p.m., and the ratios of derived values to the actual ones are shown in Table I. The results derived by the formula ranged from about one quarter to almost six times the actual values. For some groups and for some of the selected hours the calculated values approached closely the actual ones, but for the other selected hours of these groups the calculated values ranged from almost 60 per cent to over 200 per cent of the actual values, and for the same hours of other groups the calculated values ranged from 29 per cent to 130 per cent of the actual values. It is obvious that with such wide variations in possible accuracies equation 12 cannot be considered as producing reliable results in its unqualified and unrestricted manner of application.

Table I. Summation of Loads—Comparison of Results Derived by Application of Equations 12 and 14 With Actual Load Data for Seven Groups of Ten Consumers of the Commercial and Industrial Classification Selected at Random From Available Load Curves for a Day in December 1942

Derived Values in Per Cent of Actual Load

Application of Equation 12

Group Designation	Load for Selected Time of Day			
	4 A.M.	10 A.M.	6 P.M.	10 P.M.
Commercial:				
Group *A*	158	77	102	128
Group *B*	150	85	95	109
Group *C*	588	108	131	88
Group *D*	135	29	113	130
Industrial:				
Group *A′*	128	99	92	78
Group *B′*	225	91	99	63
Combination of Commercial and Industrial:				
Group A″	146	79	120	94

Application of Equation 14

Group Designation	Coincident Group Peak Load Irrespective of Time
Commercial:	
Group *A*	92
Group *B*	95
Group *C*	106
Group *D*	114
Industrial:	
Group *A′*	94
Group *B′*	88
Combination of Commercial and Industrial:	
Group *A″*	91

Application of equation 14 of the paper to actual data for determining the coincident peak load of a group of loads, irrespective of its time occurrence, produced results of considerably better accuracy than those obtained from equation 12 for the sum of loads at specified times of day; but the derived results, even from this formula, showed a deviation from the actual for the samples used as high as 14 per cent (see Table I), which is probably the range of accuracy obtainable by the application of appropriate coincidence factors to individual consumers' maximum demands without the laborious application of the formula.

Practical experience has demonstrated that no one single relationship exists for all classes of consumers between coincidence factors of a group of loads and the average of their individual load factors. Empirical relationships may be developed for certain classes of loads and for given consumer habits, and in general they will hold for similar classes of service under similar consumer habits in various communities; but different classes of consumers with different habits will establish different relationships. For example, our studies show that the relationship of monthly coincidence factor versus load factor for the combined group of large and small light and power commercial and industrial consumers in the higher-load-factor range approximates the level indicated by the curves in Figure 4 of the paper, whereas those of the residential consumers without electric ranges and water heaters are substantially lower in the coincidence-factor level in the low-load-factor range. However, the use of an electric range by these consumers changes this relationship by raising its level on the coincidence-factor scale in the low-load-factor range. Thus, different load applications and different classes of consumers will have different relationships of coincidence factor versus load factor, which can be determined with any degree of certainty only from actual test observations. No one simple mathematical formula with a given set of constants can be right for all conditions. Mathematical considerations can serve only as a means for rationalizing the results of practical observations. Such a mathematical rationalization of the relationship of coincidence factor versus load factor has been made in a thorough and classical manner by H. E. Eisenmenger in 1940 in a paper before the 55th annual meeting of the Association of Edison Illuminating Companies, but a similar rationalization is not offered in the paper under discussion.

Equation 10a of the paper represents the results obtained by the author in fitting a mathematical expression to the empirical relationship of coincidence factor versus load factor shown and first formulated in my paper presented in 1938 before the 53rd annual meeting of the Association of Edison Illuminating Companies. That relationship depicted *monthly* conditions and, as indicated, was based on 30-minute integrated demands. It is thus surprising to find a statement in the paper under discussion that the curves for coincidence factor versus load factor developed therein from an empirical formula for a daily basis should check so closely with the empirical relationship shown and developed in my paper from actual test observations on a monthly basis. It apparently does so in the high-load-factor range only because the equation fitted to the relationship depicting monthly conditions was assumed by the author to apply to daily ones. It is a well-known fact, however, that the values of monthly coincidence factors generally differ from those obtained on a daily basis. Furthermore, in the load-factor range below 20 per cent the difference is substantial between the curve labeled Δ_{30_2} in Figure 4 of this paper and the curve contained in my 1938 paper for the large and small commercial and industrial light and power service. Curve Δ_{30_2} was derived in the paper from equation 5, which in turn was based on the assumption that the exponent of Q is $-{}^1/_2$. Had an exponent of -1 been used instead, the derived coincidence-factor-versus-load-factor relationship for 30 consumer loads by the formula would have had a much closer fit in both the high- and low-load-factor ranges with my relationship developed from test observations. This would indicate that the actual convergence of coincidence factors at any given load factor toward the asymptote for an infinite number of consumers is actually more rapid than assumed in the paper. This is borne out by our test observations.

In view of these facts, the comparison made in the paper between the termination points of curves at zero per cent load factor is erroneous.

In concluding I want to commend Mr. Hamilton for the originality of his approach to a complicated subject, and I hope that he will find it possible to clarify, amplify, and particularly qualify the limitations of the formulas developed in the paper for practical applications.

R. F. Hamilton: Mr. Thomas' idea of dividing loads according to time zones is an interesting method of segregating certain portions of load curves for specific purposes. I understand that this method has been used particularly for the division of known loads on interconnected systems, and in the scheduling of generators. Its further development has many possibilities.

Mr. Lynott's comments are encouraging. He might have told you of his finding that in the coal-mining regions of Pennsylvania electric-range loads appear to have an equivalent peak-time concentration of about $6^1/_2$ hours, in contrast to approximately $4^1/_2$ hours found for Philadelphia, caused by differences in the daily habits of the consumers in the two areas.

Mr. Bary's opinions are always helpful, even if we sometimes do not agree fully. His use of equation 12 is a misapplication. It is evident that equation 12 is derived from a symmetrical load curve, one half of which would be represented by Figure 3, if one changed T to $T/2$ and p_0 to $p_0/2$. A load-duration curve is, of course, also derived from a load curve.

Suppose that you wish to find the summation at 4 a.m. of several loads for which the approximate peak times, the load-factors, and a few characteristics are known. Equation 12 is going to be of help if used intelligently. Consider each load individually. Employ a log-log duplex slide rule with fractional scales for speed in calculation, and avoid needless reference to tables of logarithms. For any loads which do not rise above a minimum value until after 4 a.m., put down the minimum value. For loads known to have two peaks, divide the load curve into two separate portions of six hours each, or 12 hours, or any number the case may warrant, with corresponding new load factor for the portion considered. Change p_0 and p to correspond to the new cycle. If a load is known to be unsymmetrical about the peak, make the necessary corrections from symmetry. Used with judgment, formula 12 (or others similar, see Table I, reference 1 of the paper) has proved very useful as confirmed by engineers who have employed it. No one can guess a curve shape for certain, but equation 12 hits closely to average values for curves with a single peak, and average values are those usually sought. Allowances can be made for deviations from average values.

As explained in the text, this equation 12 is intended to be used as an aid when considering all known facts. The greater the number of curves to be totaled the more accurate is the result likely to be. Bear in mind that, if one drops 10 coins on a table, the fact that one may obtain seven heads does not alter the fact that five is the most probable result.

Since $\Delta_Q=\dfrac{\phi_1}{\phi_Q}$, equations 2 and 5 follow algebraically from equations 1a and 1. The latter was first developed by the author in 1916 in London to meet the specific problem of determining the peak loads on substations and generating plant for a railway electrification with about 90 trains operating at one time. (The derivation is repeated in Appendix H, reference 1 of the paper.) It has been used since by engineers in many parts of the world. With α equal to two, it conforms to the theory of least squares as applied to variations from average values. In practice, α has often been increased above two in value, as discussed in the paper. I do not feel that Mr. Bary's suggestion that α be made equal to one in value will be a solution to his difficulty.

Instead of

$$\Delta_Q=z+(1-z)Q^{-1/2} \tag{5}$$

I would suggest that he try

$$\Delta_Q=1/Q\{1+[(Q-1)^2z^2+(Q-1)(1-z)z]^{1/2}\} \tag{5a}$$

which also follows from probability laws, but based on the root-mean-squared value of all load combinations. Equation 5 gives higher peak values than equation 5a, and is preferred by me because it seems to agree more uniformly with peaks as actually encountered in practice. The time of measurement of peaks also enters into these relations. (See Table II, reference 1.)

Zero load factor does not exist. The nearest approach to it, of which I can conceive, would be that of a short circuit rapidly cleared. Mr. Bary's tests were made with peaks of one-half hour duration, or $p_0=0.021$. (There was presumably a peak each day commensurate in value with that of the monthly peak.) This load factor, 0.021, would have been the minimum load factor possible, if there were no energy involved other than that of the peak period, which was hardly the case. His test points show one load factor as low as about 0.06.

At a load factor approaching zero, equation 5 would show a value of $Q^{-1/2}$. Equation 5a, under the same conditions would give $1/Q$. For these limiting conditions near zero load factor, I think that equation 5a is the more accurate, because it states that the peak has the value of the peak of one load.

The Δ_{30} curves as presented are intended to be average values for a daily cycle. There is not, in my opinion, a constant ratio between the value of the average of the daily peaks and the highest peak within a month. This ratio is a characteristic of each individual load (see Figure 12, reference 1 of the paper). Should the monthly peak exceed the average daily value by ten per cent, its effect on the summation would be of the order of $10/Q$ per cent increase, since the probability of two or more of these peaks' occurring together is negligible for usual conditions.

Neither Mr. Bary nor Mr. Eisenmenger in the papers cited by Mr. Bary have made any reference to the variation of Δ_Q with Q, since presumably they were considering only large groups of customers. Those who are familiar with diversity factors know that this variation is of fundamental importance.

From tests summing 30 individual load curves in each of various load-factor groups on a unit peak basis, Mr. Bary produced Δ-ϕ curves of the general shape shown in the paper, giving explanations for the shape.

By making certain assumptions, notably the introduction of a concept which he termed a "distribution load factor," Mr. Eisenmenger elaborated on Mr. Bary's conclusions, in general demonstrating reasons why such curves are possible.

Starting with the concept of p, an "equivalent time" or fraction of a daily cycle in which the peaks may be considered to be concentrated and with the further assumption that there exists a probability relationship between this time and a function of the load-factor, the author has produced by simple mathematical solutions Δ-ϕ curves similar to those Mr. Bary made from tests. These concepts are relationships which can be measured.

It has already been demonstrated that the use of p reconciles the results of tests which seemed to be at variance for several types of equipment and load groupings.

I believe that you will find that the resolution of these problems into a relationship of the number of units involved and the equivalent time of peak concentration is a novel approach, which is workable in terms of ordinary units. By no means can it be said that I believe the subject to have been fully covered. The possibilities have only been touched.

You may have noted that Mr. Bary has read a few statements into the paper which do not appear. These were made inadvertently, for Mr. Bary has always been very co-operative. In all fairness to him I must state that the constants of equation 10a were the result of compromise and therefor do not fit his curve exactly.

The Effect of Weather on the System Load

Discussion and author's closure of paper 44-106 by Henry A. Dryar, presented at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944, and published in AIEE TRANSACTIONS, 1944, pages 1006–13.

Uriah Davis (Commonwealth Edison Company, Chicago, Ill.): Mr. Dryar's paper is an excellent analysis of the effects of weather on the total load, and the data obtained from such a breakdown, where all the conditions are known, should be useful for future planning.

From an operating point of view, it appears to me to be a waste of time to go into so much detail making a forecast one day in advance with all of the factors unknown. This is brought out by the author in that he states it is revised about two hours before the peak and based on weather data that is known at that time.

Total load predictions, to have any value, must be based on the weather conditions prevailing up to the shortest time that is available to bring in the necessary boilers and units to carry the load. With this last-minute weather information, the time of day, and day of the week all known, it is a very easy matter to predict the maximum load directly in kilowatts within the two per cent limits, which the author considers to be good.

The advance scheduling of the order of use of all available capacity is possible and necessary, and must be revised each time there is a change in outage of major boilers or units. With this order known, and the last-minute load estimate of maximums prepared, the scheduling of station loadings, for best economy, is a very simple matter.

Forecast of load should be made for the morning, afternoon, and evening.

Practically all systems include a spinning reserve in their load estimates. This reserve should be large enough to cover the loss of the largest line or unit, and probably will be at least ten per cent of the load. If the error in forecasting is within two per cent, the error is easily absorbed by the reserve.

Henry A. Dryar: With reference to the comments of Uriah Davis on the paper entitled "The Effect of Weather on the System Load," wherein he questions the necessity of a load forecast one day in advance, we find this a desirable practice, essential to the economic scheduling of generating capacity on the systems of the Pennsylvania–New Jersey interconnection and the reliable operation of the individual system. Furthermore, it is considered essential also that each generating-station superintendent be informed of the expected demands on his station, so that he in turn may schedule his apparatus for operation or for maintenance.

Whereas the daily system peak load varied more than 12 per cent over a period of several months, the revised load estimate made the following morning in 92 per cent of the days involved did not depart more than two per cent from the previous day's forecast. The change in the capacity schedule in these instances involved but one generating station and concerned the operation of oil-fired equipment at that station.

I cannot agree with Mr. Davis in his comment that it is a very easy matter to predict the maximum load directly in kilowatts within two per cent of the actual. Keeping within a maximum error of two per cent in load estimating from past experience will prove rather difficult, in my opinion.

A-C Contactors for Aircraft

Discussion and authors' closure of paper 44-88 by F. J. Russell and A. P. Charbonneau, presented at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944, and published in AIEE TRANSACTIONS, 1944, September section, pages 613–16.

W. A. Barden (major, United States Army, Wright Field, Dayton, Ohio): The authors have prepared a very interesting and informative paper which thoroughly covers the work that has been accomplished to date in the development of subject equipment. The paper describes contactors using d-c-energized magnets. It is possible that a-c-actuated contactors would be preferred in order to eliminate the need for any d-c wiring in an airplane. This does not infer that there would be no d-c power used in such an airplane. Possibly a weight saving in the complete system could be realized by handling all power distribution as alternating current with suitable individual rectifying equipment built into those installations where d-c power proved to be essential.

Contactors actuated by a-c power could make use of shaded-pole a-c magnets or of small torque motors. In either event such a device might result in a contactor weighing somewhat more than its d-c-operated counterpart. However, if a saving of weight and a decrease in vulnerability were attained in the over-all system, the aforementioned greater weight of individual contactors would be justified.

The paper covers the question of enclosures in connection with keeping dust and atmospheres of high humidity away from the contacts. It also deals with the difficulties encountered in the interruption of arcs at high altitude and the necessity of incorporating barriers in the contact enclosure in order to provide insulation between phases. The purpose of this discussion is to point out that all of these problems could be greatly reduced and the effects of dust and humidity completely eliminated by enclosing the entire contactor in a pressurized hermetically sealed container which carried an inert gas under pressure such that arcing would be practically impossible.

The contactors described in the paper were developed to operate at altitudes as high as 40,000 feet. We now are confronted with the possibility of operations at altitudes well in excess of this figure. Under these conditions hermetically sealed pressurized contactors would appear to be an actual necessity in order to insure absolute reliability of operation under all conditions.

The authors are to be congratulated on the excellence of the contactors which they developed. It is hoped that contactors soon will appear which will incorporate the features pointed out in this discussion.

A. W. Nash (El Segundo, Calif.): This paper is timely in that the probable coming eminence of the 400-cycle a-c system will demand that numerous new types of switchgear be developed. Relays and contactors of various types are frequently the heart of such systems.

This present paper presents a rather brief and concise discussion of general requirements of 400-cycle contactors without going greatly into the detail of the design of these contactors. For instance, it is stated in the paper that it is desirable that the control circuits for these contactors be operated by direct current. No discussion nor description is presented regarding attempts at developing 400-cycle coils.

The convenience, reliability, and light weight of the auxiliary d-c power supplies now being used on aircraft indicate that such power supplies may be used in the future instead of the earlier proposed method using transformers and rectifiers operating from 400-cycle alternators. These auxiliary power supplies probably will be used for starting the main engines and then turned off for economic reasons. This would indicate the need for 400-cycle control circuits as well as power circuits. If any work has been done along this line by the authors, it would be beneficial to readers of this paper to have some description of this work included in the paper. If no work along this line has been done, extensive development work should be carried out to obtain the lightest possible a-c-operated contactors.

The convenience realized in past use of switch-type d-c circuit breakers indicates the desirability of developing a circuit-breaker type of contactor for two-circuit use. Such circuit-breaker contactors would be used both for main-bus and for equipment-feeder circuits. The use of overload relays in conjunction with power contactors is outlined in this paper. It is believed that some additional weight might be saved by combining these two items into one unit.

Some thought should be given to the possibility of designing nontrip-free-type circuit breaker contactors. In all of these circuit-breaker-type contactors considerable attention must be given to the current-interrupting capacity of the contactors. It is noted from Figure 5 of the paper that present considerations have been given to circuits with current-interrupting capacities as high as 4,000 amperes. It is thought that this is definitely a step in the right direction.

F. J. Russell and A. P. Charbonneau: Both Major Barden and Mr. Nash bring up the question of a-c actuating magnets. Obviously, a-c magnets could be provided. Studies of the design of magnets to perform the same work as the d-c magnets now used indicate that their weight would be from two to three times as great as that of d-c magnets. This would increase the weight of the switches to 150 to 200 per cent of the present weight. Studies of the over-all electric systems of the plane would be required to determine whether such weight increases are justified. If, as Major Barden points out, reduced vulnerability of the system results, some increase in weight may be justified. In this connection it may be pointed out that a-c magnets do not provide the same operating voltage range as d-c magnets, since they will not hold closed below approximately 50 per cent of normal voltage as compared with 25 per cent or less for d-c magnets, and, in order to prevent overheating on an increase of ten per cent above normal voltage, closing voltages required will be about 85 per cent of normal voltage. In other words, the voltage regulation of the a-c system would have to be held much closer than required on the d-c system to obtain satisfactory operation.

The question of hermetically sealing the contactors introduces a whole new set of problems in materials as well as construction. The writers' experience would indicate that the desired results would not be obtained by the provision of a sealed enclosure over the present structures but would involve a completely new approach in the materials and design of the component parts. Since all of the arcing tests were made at pressures corresponding to 50,000 feet or more, there is little hope that very decided improvement would be obtained as far as arc extinction by hermetically sealing is concerned.

An Aircraft Differential-Voltage Cutout

Discussion and author's closure of paper 44-108 by O. C. Walley, presented at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944, and published in AIEE TRANSACTIONS, 1944, September section, pages 632–4.

M. F. Gunderson (nonmember; Los Angeles, Calif.): Of the two fundamental improvements credited to the differential-voltage cutout, only the first one is entirely justified. Chattering of the conventional-type relay has been a constant source of trouble in multimotor aircraft, but it rarely occurs in single-motor aircraft. In a single-motor aircraft, the conventional-type relay will chatter because of low generator voltages encountered under two conditions of operation:

1. Low voltage due to faulty regulators—this condition is possible, but experience has shown that voltage-regulator malfunctioning usually results in high generator voltages rather than low voltages.

2. Low voltages due to low generator speed may be encountered during ground operation. The particular combinations of generator voltage, battery voltage, and loading conditions necessary to produce chattering are rare.

However, elimination of cutout chatter in multimotor airplanes is an important improvement.

The second fundamental improvement listed is of importance when the system voltage is reduced somewhat below the normal voltage at which the conventional cutout would operate. However, the particular example of the possible benefit of this improvement mentioned by the author is not clear. Engine speeds on all aircraft during flight, as far as we know, are high enough to run the generators at rated speed.

Of the three additional improvements claimed for this relay, the following comments are in order:

1. A reverse-current relay, or generator-control relay, which is in use at the present time and conforms to *AN-S-7* (drawing *AN*3024) will protect the system when a short circuit occurs in the generator.

2. It is evident that a tight enclosure is necessary with a design incorporating several rather delicate relays, as in this one. However, from the standpoint of heat dissipation, the tight enclosure is not desirable. The generator-control relay mentioned in the preceding paragraph is not enclosed. Its operation in aircraft has been satisfactory.

3. Other relays have been designed and are being used which are not adversely affected by vibration encountered in airplanes.

The conclusion that the differential-voltage cutout has "none of the disadvantages of the automotive type of cutout" does not appear to be entirely justified from the description of the design and operation given in this paper.

The probability of short circuits on the bus or load side of the cutout in any aircraft electric system in most cases is greater than on the generator side of the cutout. A short circuit sustained a few seconds on the load side of the cutout will cause the conventional-type relay to chatter and the contacts to weld. Evidence introduced by the author of this paper does not indicate that the differential-voltage-type cutout has overcome this trouble.

AN specification *AN-S*-42 (amendment 1, dated April 3, 1944) for a differential-voltage cutout does not state or in any manner imply that it is "intended to *supersede* the automotive type of cutout" as is stated by the author; that is, to be interchangeable does not mean to supersede.

Specification *AN-S*-42 (*I*-2) states: "Interchangeability—The relay switch covered by this specification is interchangeable functionally and physically with switches covered by specification *AN-S*-7."

O. C. Walley: The opening sentence of my paper restricted the contents to aircraft electric systems containing two or more generators. No attempt was made to discuss the merits of the conventional or automotive type of cutout when used on aircraft systems containing only one generator. The automotive industry has years of experience to prove that the conventional type of cutout is entirely satisfactory when used with a single generator system.

Mr. Gunderson has attempted to discredit the second advantage claimed for the differential-voltage cutout on the basis that the generators are always run at rated speed. There is evidence to show that on long flights, such as ferrying missions, generators have operated below rated speed with the result that the batteries became completely discharged during flight.

If Mr. Gunderson will review specification *AN-S*-42, with which he seems to be familiar, he will find that the short-circuit protection on the load side of the cutout is not one of the many functions required of this differential-voltage cutout.

With regard to the heat-dissipation problem when the cutout is enclosed, this same problem had to be considered on the conventional cutout. Airplane manufacturers have mounted these relays in very small unventilated compartments.

While it is possible to design relays which are not adversely effected by the vibration test specified in specification *AN-S*-7, tests have shown that there are conventional types of cutouts now in use, made by more than one manufacturer, which do not pass this test.

At the time this paper was prepared the tentative specification stated: "This specification supersedes the current issue of *AN-S*-7 which superseded United States Army specification 94-32278 for aeronautical use."

Aircraft Fuses Must Protect

Discussion and author's closure of paper 44-90 by John C. Lebens, Jr., presented at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944, and published in AIEE TRANSACTIONS, 1944, August section, pages 581–5.

Julian Rogoff (nonmember; Burndy Engineering Company, New York, N. Y.): Considerable misunderstanding of both the application and function of limiters in aircraft electric circuits seems to be widespread. This misunderstanding is reflected in technical papers presented to the Institute such as that by J. C. Lebens, Jr.

In the first place it should be clearly understood that the function of the limiter is not to protect equipment or even to protect the electric circuit against sustained or transient overloads. The limiter provides short-circuit protection for the electric circuit. Other devices are available and should be used to protect equipment attached to the circuits. The selection of limiters to provide short-circuit protection should be such that the time-current fusing characteristic of the limiter is well above the values which may be expected of any sustained or transient overloads at that point in the circuit.

The importance of providing short-circuit protection in the electric circuit, as well as of designing an electrical aircraft circuit that can be thus protected, is becoming increasingly apparent.

Faults which may occur in electric aircraft circuits, usually as a result of combat damage, may be "opens" or "grounds." In an electric system which provides parallel paths of current flow from the current sources to the electric loads the "opens" are self-correcting. "Grounds" may or may not be self-correcting. Undoubtedly the majority of "grounds" caused by gunfire will consist merely of a few strands of cable brushed out by the projectile coming in contact with the airplane structure. Such faults will burn clear rapidly and automatically become "opens." However, the possibility always exists that some "grounds" will not burn clear, especially those which may result from the grounding of junction points where cables are joined by means of terminals or connectors. Such "grounds" may weld shut to the airplane structure and result in sustained short circuits, which will depress the voltage in the network to such an extent that the connected loads will become inoperative.

Limiters are high-melting-point fuses expressly designed to limit the duration and violence of faults in the electric circuit. Originally they were designed for this use in underground networks of utility systems where, theoretically, the available short-circuit current was more than ample to "burn off" any fault but where, as a practical matter, many faults would sustain themselves and "roast" the cable insulation for great distances. In aircraft the need to clear the electric system of faults is of even more vital necessity. Even though, theoretically, the fault should be self-clearing, it may sustain itself and cause gun-turret motors to stop functioning, landing gear to cease operating, and so forth. A device such as the limiter of highly stable characteristics, little affected by ambient temperatures, and very light in weight is almost essential as backup protection for the electric circuit.

In order to take advantage of the ability of limiters to clear short circuits rapidly, the aircraft electric circuit should be designed with parallel paths of current flow from the current sources to the electric loads. Such parallel paths may be obtained either by multiple cables per conductor, or by an electrical network. In either event each parallel path of current flow should be protected by limiters. Careful co-ordination of limiters is of utmost importance to prevent fusing of the wrong limiter in the event of a fault. The fact that limiters are true to characteristic and that the current required to fuse the limiters for any particular time varies only slightly means that limiters can be co-ordinated more successfully than thermal breakers or low-temperature fuses, which have characteristics with greater "spread."

Proper utilization of limiters in aircraft electric circuits will provide safer more continuous operation of the circuits and thereby will expedite the trend toward more completely electrified airplanes.

A. W. Nash (El Segundo, Calif.): The impression gained from reading this paper is that it is an attempt to revise an outmoded device. Although previously extensively used, the fuse now is being replaced in the home, in industry, and in aircraft by the more versatile circuit breaker.

It appears from reading the paper that much emphasis is placed on the matter of electric-equipment protection. Experience with aircraft electric equipment has indicated that protection of this equipment is not only difficult but also generally unnecessary. The exceptions to this statement, such as motors, generators, and inverters, are best protected by means of thermally operated devices mounted on the individual equipment item. It is difficult to visualize any advantage to be gained from using a fuse for this protection in place of the presently accepted recycling thermally operated circuit breaker. This device has proved most useful in protecting electric equipment from various types of overloads, as well as in resetting in case of the nuisance outages that invariably occur for one reason or another. The author does not mention these devices in his paper, proposing instead the use of a time-delay fuse. Any attempt to provide protection for aircraft electric equipment by means of replaceable fuses would require an inordinately large variety of sizes and types of fuses. This would make the stocking and replacement problem in the field cumbersome and difficult.

It has been found from experience that, if the few equipment items outlined are protected suitably by integrally mounted thermal circuit breakers, the remainder of the airplane can be protected adequately by means of circuit protection. Since it is also difficult and sometimes impossible to place the circuit protective device in close proximity to the majority of the circuit which it protects, the effect of ambient temperature will, in general, be the same for any type of protective device used. For this reason the combining of circuit protection and switching functions into one switch-type circuit breaker is extremely convenient. This tends to substantiate the opinion that the future use of fuses in aircraft is of a limited nature. For this reason it is not believed that this paper is particularly timely. The bibliography referred by the author is impressive in its size but appears to be a bit unwieldy and partially unnecessary.

B. W. Jones (General Electric Company, Schenectady, N. Y.): Mr. Lebens outlines a theoretical arrangement of motors, control, and wiring wherein, if all the assumed conditions obtained, he would accomplish the desired protection by means of the type of fuse which he has in mind. But there are some practical considerations which should not be overlooked.

Since there will be several sizes of motors on the plane, there also will be an equal number of sizes of fuses. When one set blows, there will be no assurance that the service man will put the same size fuse back. Also, there may be no permanent convenient record that a given size fuse is needed for a given motor. When it is realized that this servicing will be done by all kinds of persons in all parts of the globe, this mix-up in fuse sizes appears as a real problem.

It was recommended in the paper that the fuse always should be in the same ambient temperature as the motor, the control, and the wiring. Since the layout for the wiring, the motors, and the control in the airplane will be done by many engineers and draftsmen, each will put these devices where they best fit into his plan, which means that there is no assurance that this assumed condition will be carried out. It would appear to be more logical to make the protective device insensitive to ambient temperature and then select the protective device, so that it would give good protection based on some assumed average ambient temperature; the actual ambient will go above and below this, but the average life of the insulation automatically will adjust itself to this varying condition. Then the airplane designer will have a free hand to design his airplane without compromising what he is trying to accomplish.

It was assumed by the author that the proposed type of fuse had the necessary interrupting ability. But the amount of short-circuit power that must be handled today on these planes is no measure of what may have to be handled tomorrow. The standard types of fuses now on the market have their interrupting limitations, but the time-delay fuse shown in the author's paper is so constructed that approximately half of its length is mortgaged for this time-delay feature, which means that its interrupting ability is correspondingly impaired. This point is not just theory. It has been checked by test. Therefore, if it has been found that this type of fuse could not provide the necessary interrupting ability, then it should not be used. To start such a practice now, when the available short-circuit currents may be low and when the fuse may be able to handle the present power, might lead to an experience similar to that of many industries which put in low-interrupting devices when their plants were small only to discover later on that these low-interrupting devices had become a real problem.

The author makes a distinction between the inverse time characteristic of the required fuse, depending upon whether it is protecting the branch circuit or the motor. The current density in the copper of the branch circuit is sufficiently near to that in the windings of the motor, so that no distinction need be made. The thermal capacity of motors, which is high because of their iron mass, need not be the basis for using two kinds of fuses, because this difference is a transient effect, and because it applies only for small degrees of overcurrent. The insulation in the branch circuit can absorb this transient temperature difference without injury. This has been done for years.

In conclusion I think that Mr. Lebens' objective is very good in that he is trying to get the maximum of protection with the smallest and lightest type of device, but the practical considerations of his proposed method brings out certain features that should be well considered.

J. M. Wallace (nonmember) and **J. C. Cunningham** (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The need for the time-delay fuse as described by Mr. Lebens in Figure 3 is predicated upon the protection of electric equipment by the fuse. There is no question but that a fuse application as shown in Figure 3 of the paper, where the fuse has a thermal conducting member extending into the protected equipment, will do a reasonably good job of supplying overload protection to the equipment. The intimate thermal contact between the fuse and the equipment will allow closely controlled characteristics of the fuse which are directly dependent upon the temperature of the protected apparatus.

This type of protection should be built in by the manufacturer of the equipment, and a specific fuse design should be provided by the fuse manufacturer for this service. Only by carefully controlling the characteristics of the individual application can protection for the equipment be obtained. If sufficient short-circuit interrupting capacity can be built into the fuse unit to take care of faults, a complete equipment protective device has been provided.

In discussing the difficulty of replacing an equipment protective fuse in flight Mr. Lebens states that "the protective device will function only when the equipment is worthless." If the fuse characteristics were selected on this basis, it would not result in equipment *protection* but merely in equipment *isolation*. In this way protection would be afforded only to the supply of power to other equipment. For electrically driven equipment which may become jammed and later freed in flight *equipment protection* would appear desirable to save the equipment for further use. Particularly for inaccessible equipment, a protective device with automatic or remote manual reset would be more desirable than a fuse. This feature could be provided in a bimetal protective device.

The paper shows the saving in weight possible by the use of the time-delay fuse for circuit protection which is described in a specific case as weighing only 20 grams against 121 grams for the ordinary fuse. In Figure 1 of the paper it is seen that the over-all length of the fuse can be cut by 75 per cent, if the time delay and thermally operable current interrupter be omitted and only the "short-circuiting strip" or limiter used. In addition the size of the mounting is cut more than just proportionately, because the limiter generates relatively little heat and, consequently, the size of the fuse clips can be reduced as well as the over-all length.

In considering protection for the distribution system, that is, the wiring, Mr. Lebens points out that fault resistance may reduce appreciably the short-circuit current, especially in low-voltage systems. Combat experience and gunfire tests indicate that this reduction usually will occur but that self-clearing results within a few seconds. It appears very doubtful that either time-delay fuses or limiters would clear a sufficient number of faults in present low-voltage systems to justify their use.

The magnitude of short-circuit current in higher-voltage systems is affected less by fault resistance. Also, the ratio of minimum fault current to maximum load current is much higher in proposed a-c systems than in present systems. The probability of a low-current sustained fault is very remote, and there is little danger that limiters will be unable to clear or will be destroyed. This permits the use of fast-acting fuses or limiters without the additional weight or space required for time-lag elements. Even in these systems there is considerable doubt that circuit protective devices are necessary, although more information on stability characteristics and ability to recover from faults of several seconds duration is needed before any conclusion can be reached.

Thomas B. Owen (Douglas Aircraft Company, Inc., Santa Monica, Calif.): Protection of aircraft wiring systems always has been a debatable subject. Actually, there is quite a bit of room for discussion. There are, however, several basic facts regarding aircraft generating systems which form the basis for any discussion of circuit protection, and the author of this paper has stated these facts incorrectly.

A d-c aircraft generating system consists of one or more generators paralleling one or more storage batteries. In the majority of cases nowadays these generators are of 100-ampere capacity with many 200-ampere generators in service, and 300- and 400-ampere generators are projected. The short-circuit currents available are very large; it is doubtful if a single circuit exists on the airplane where the short-circuit currents are under 100 amperes. I have made oscillographic records of bus short circuits on an airplane using two 200-ampere generators and a 68-ampere-hour storage battery where the sustained short-circuit current was over 2,000 amperes. Similar evidence is available from other sources. In the list of references Mr. Lebens has several papers which will confirm my observations.

On 400-cycle 208-volt a-c systems the line and fault impedance undoubtedly will limit the current flow. I have no quantitative data on the currents available on a-c aircraft systems, but a paper entitled, "Electric-Circuit-Burning-Clear and Damage Phenomena on Aircraft Structures,"[1] does show that the currents here will not be small.

In any event the procedure proposed by Mr. Lebens, that of leaving a short circuit on until the aircraft returns to its base, would not be tolerated on an airplane. The presence of explosive vapors in some compartments and the possibility that the fault might be against fuel or hydraulic fluid lines makes it essential that faults be cleared as soon as possible. This does not admit of any argument; it is a well-established principle in aircraft practice.

It is a common fault among those considering aircraft-circuit protection to say that the protective device should have the same time-current characteristics as the wire it is to protect. Unfortunately, aircraft wiring is installed in bundles; a wire in the center of the bundle has a different characteristic from a wire on the outside. Also, the bundle may be routed in areas of good and poor ventilation successively and may have to run in areas where the ambient temperature is high. The question then becomes: Which section of wire are you going to use for your basis? Obviously, you must protect for the worst condition, and in the interest of standardization a particular type and size of protective device must serve for a variety of installations. This means that the same protective device must be used for the cool and hot installation and the worst possible case protected. Let it be said here that this last statement is the key to most aircraft installations, and the worst case must be determined and provided for. This results in overprotection; but this is unavoidable in the same manner that the weight of a stressed spar in the wing is unavoidable, since it must withstand the maximum stress to which it may be subjected.

The advantages to be gained from grouping protective devices in one location, such as accessibility, and ease of replacement and inspection, so far outweigh the theoretical advantages due to matching the wire and protective-device characteristics that any change in present practices is unlikely. Incidentally, grouping the protective devices together does not require that the wire to the distribution panel be protected, as this wire is considered part of the bus.

In conclusion, I believe that Mr. Lebens should consider the peculiarities of aircraft and make his design observations on that basis. Undoubtedly, aircraft designers may learn a great deal from commercial designers, but the reverse is even more true. The close tolerances necessary in aircraft, together with the measures taken to reduce weight and increase strength, have produced superior designs, which can well be copied industrially.

REFERENCE

1. ELECTRIC-CIRCUIT-BURNING-CLEAR AND DAMAGE PHENOMENA ON AIRCRAFT STRUCTURES, C. M. Foust, J. G. Hutton. AIEE TRANSACTIONS, volume 63, 1944, April section, pages 198–204.

A. H. Powell (General Electric Company, Philadelphia, Pa.): Certain considerations tend to discount the suggested universal use of a time-delay fuse in aircraft systems. The industry in general knows that an interrupting device with time-delay characteristics is most suitable for protection of aircraft motors, generators, and other devices having relatively large masses of metal to absorb the heat generated by passage of current through the associated coils. (This point is stressed in the recent AIEE paper by V. G. Vaughan,[1] which describes an automatic motor protector.)

However, when the protection of the distribution circuit in modern military aircraft is under consideration, it is readily apparent that continuity of service in each circuit must be maintained at all times, unless the circuit is rendered inoperable by a fault such as battle damage might produce. Therefore, a fuse with a high-temperature melting link is most suitable, for its characteristic is similar to that of the associated conductors. Reliable fuses of this design are available, which will carry full load without exceeding a specified temperature rise, also will satisfactorily clear normal overload, and, of course, will not destroy themselves on low current. Such fuses now used on power systems are described by Prince and Williams in their 1939 AIEE paper.[2] Furthermore, for protecting aircraft circuits a high-temperature melting link would be preferred because of the wide range of ambient temperatures encountered in operation. A low-temperature melting link is generally unsatisfactory for this purpose because of the large changes in melting time-current characteristics due to variations of the ambient temperature.

The author further seeks to differentiate between a fuse and a limiter by stating that the fuse will melt on currents only slightly in excess of its normal capacity, whereas a limiter will melt at a current greatly in excess of its safe carrying capacity. The American Standards Association defines a fuse as an overcurrent device with a circuit-opening fusible member directly heated and destroyed by the passage of current through it. No distinction is made between a device having a low melting-temperature link or a high-temperature melting link and, therefore, the so-called limiter is in reality a fuse. The word "limiter" is used by certain manufacturers to designate an interrupting device incorporating a reduced section link suspended in air and connected in cables of network systems, so as to provide a point at which the circuit may be interrupted when the system develops a fault. Although the devices may be designed to have typical fuse characteristics of time-current relations, they do not have definite arcing times. Such a device will, of course, not be suitable for aircraft use, for it is essential in the proposed systems that melting and arcing time be controlled closely and definitely known.

Though the time-delay fuse described in this paper undoubtedly can be used in certain applications, the satisfactory performance of the modern electric system in aircraft demands a fuse with "fast" characteristics obtainable by using a high-temperature melting link surrounded by an arc-interrupting medium and correctly designed to operate under the specified conditions with a reasonable and safe temperature rise. Furthermore, recent information indicates that proposed aircraft systems will develop high short-circuit currents which must be interrupted satisfactorily by the fuses used. It would be interesting to know the interrupting rating of the fuse described in this paper when used on 208-volt 400-cycle systems.

REFERENCES

1. PRINCIPLES OF AIRCRAFT ELECTRIC-MOTOR PROTECTION, V. G. Vaughan. AIEE TRANSACTIONS, volume 62, 1943, December section, pages 760–5.

2. THE CURRENT-LIMITING POWER FUSE, D. C. Prince, E. A. Williams, Jr. AIEE TRANSACTIONS, volume 58, 1939, January section, pages 11–14.

W. K. Boice (General Electric Company, Schenectady, N. Y.): The author has presented some important and interesting principles regarding application and design of fuses. Certain of his conclusions, however, require qualification, particularly in respect to use of fuses in military aircraft.

The paper recommends that fuses remove electric equipment from the system, only after it has ceased to function or has become a hazard. If fuses are applied in this manner, it is not necessary to have fuses in locations any more accessible than the equipment. However, in many cases this sort of protection is not acceptable. More satisfactory protection can be obtained by means of devices which remove the equipment just before it is destroyed. This can be done accurately by lightweight devices which respond to actual temperature in the windings of the equipment and which will permit subsequent use of the apparatus, after the temperature has been restored to a safe value.

The impression is given in the paper that increases in system voltage tend to increase short-circuit currents. However, changes in system voltage generally are accompanied by changes in system characteristics, which often do not result in increases in short-circuit current. Certain 208 Y/120-volt 400-cycle, a-c systems now under study have maximum short-circuit currents in the order of 4,000-amperes, whereas 8,000-amperes are available on some aircraft using 27-volt d-c systems. The effect on short-circuit currents of a change in system voltage can be determined only after a study of the particular system under consideration.

The relative ease of co-ordination of certain types of high-temperature fuses apparently is attributed by the author to their short operating times. However, this ease of co-ordination must not be considered to be inherently due to these short clearing times. It is caused primarily by the absence of overlapping of characteristics. It is only necessary that the total clearing time, which is the sum of melting and arcing times of the fuse which removes the fault, be less than the time required for the fault current to melt or damage any other fuses in the circuit.

The grouping of circuit protectors in aircraft has many advantages which must not be overlooked in spite of the disadvantages mentioned in the paper. Among the most important of these advantages are convenience in checking and servicing protective equipment and assistance in locating faults.

The paper stresses the necessity of proper application of fuses. This requirement cannot be overemphasized. Careful study of each fuse application will be well repaid in terms of improved protection.

John C. Lebens, Jr.: Mr. Rogoff's discussion of the author's paper is most welcome in that he substantiates the definition of limiters as being "high-melting-point fuses expressly designed to limit the duration and violence of faults in the electric circuit." Since he admits that the only purpose of the limiter is to "provide short-circuit protection for the electric circuits" and that other devices must be used in conjunction with the limiters to give complete protection, he can have no quarrel with the time-lag fuse described, because it incorporates in the one enclosure the limiter Mr. Rogoff states is necessary for short-circuit protection along with the thermally operable current interrupter needed to furnish complete protection at the lower overloads.

Mr. B. W. Jones's impartial, clean-cut discussion is appreciated. It is true that the author assumed conditions, but these conditions are fairly representative of the airplane electric circuits of today, as well as of the trend to be expected.

The question of replacing the fuse with the correct one by untrained help is serious, but it finds its solution in commercial fuses. With minor changes in the fuse cutout itself overfusing is eliminated. These devices have been handled without complaint by housewife, druggist, and electrician so that certainly such a system would not be too complicated for the airplane maintenance crews no matter how meager their training.

The argument that the protective device should be made insensitive to ambient temperature, so that the engineers and draftsmen can place them where convenient in the plane, is not sound. In the final analysis the electric protective device is as important as the sprinkler system in a building and should be given the same consideration. No one would think of pushing the sprinkler heads into the back room, so they would not mar the beauty of the auditorium. They come first and so should the electric protective devices. Any other treatment of them makes the whole situation a farce and should not be countenanced.

The interrupting capacity of the time-lag fuse has been proved in industry but, since we have not limited ourselves to any particular dimensions, certainly this difficulty can be overcome.

As Mr. Jones indicates the effect of the thermal capacity of the motor iron is a factor only at the low overloads. However, if we are to have complete protection without needless blows, the time-lag of the protective device at these loads must be greater when protecting a motor than when protecting a circuit wire. Hence, the time-current characteristics of the protective device used for the two applications will not be identical.

The author heartily agrees with Mr. Owen's opinion that the subject of aircraft wiring systems always has been debatable. However, unfortunately Mr. Owen failed to get a true picture of the protective device described in the paper. Not only will this protective device interrupt the short-circuit current immediately, but also it will function protecting the wiring and its associated equipment when damaging overloads not approaching short-circuit values are present. Therefore, Mr. Owen's statement that the procedure proposed is "that of leaving a short-circuit on until the aircraft returns to its base" has no foundation in the paper.

Mr. Owen, in his discussion, emphasizes a fact which generally has been ignored by the aircraft engineers in choosing a protective device. He points out that the wires in the circuit are grouped in bundles and passed through high- and low-ambient-temperature spots in the same plane, yet no discrimination is made in the selection of the wire insulation or wire size for these unusual ambient-temperature conditions. The author contends that to obtain the most satisfactory installation with the least weight, all these factors should be considered in the electric layout. Then with sectionalizing time-lag fuses located in the same ambient temperature the fuse will function protecting the wire, instead of the wire itself being destroyed.

The advantage of grouping protective devices in one location is not as great as most people generally contend. The trend in modern aircraft is away from a single panel, because such grouping makes the electric system extremely vulnerable to a single gunshot through the panel. If we use a protective device that only blows when there is a case of trouble, the advantage of ease of replacement no longer can be considered, because the wiring itself should be checked before the fuse is replaced. Under these conditions the sectionalizing fuse located close to the section of wiring or the equipment it is protecting should simplify the servicing by its very location.

Mr. Owen, like so many aircraft electrical engineers, tried to explain away the rather inadequate protective job now being done by blaming it upon the close tolerances that are necessary in aircraft. However, if the protective devices are designed correctly, many of the limitations which have been imposed upon them resulting from the improper operations of poorly designed devices would be eliminated, thereby placing the whole system on a sound engineering basis.

Mr. Nash's opening statement in his discussion is argumentative to say the least and hardly worthy of an engineering discussion. The statement that "the fuse is now being replaced in the home, in industry, and in aircraft by the more versatile circuit breaker" is incorrect; the fact is that circuit breakers actually are being torn out of numerous installations and replaced with fuses, because the circuit breakers were found to be unsatisfactory. However, his discussion of the relative merits of fuses and circuit breakers has no bearing on the article Mr. Nash intended to discuss.

The purpose of the paper is to design a protective device which will "permit the equipment to function as long as it can supply any useful work but to remove it from the circuit when it has ceased to function or has become a hazard." This desire is not the author's alone but that of the Army Air Forces as well. If such an ideal be realized, there is no need for a recycling protective device, because often recycling indicates sloppy engineering. The protective device designed or selected improperly operated when there was not a case of trouble so the undamaged equipment was returned to the line to try again.

Recycling in military aircraft hardly can be considered satisfactory, because the plane is at the mercy of the enemy while the crew sits around waiting for the cooling cycle to be completed. We all realize the problem is a difficult one and cannot be solved by any one man, but certainly we never will reach the solution, if we assume Mr. Nash's attitude and, in his words, "become bewildered and give up in disgust."

Mr. Wallace and Mr. Cunningham in their discussion concur with the author's opinion that complete electrical protection can be obtained by the time-lag fuse, when the fuse is placed in intimate thermal relation with the equipment it is protecting. They raise a shadow of doubt as to the short-circuit interrupting capacity of such a fuse. Since this fuse is an adaption of a device already proved in industry to have sufficient short-circuit interrupting capacity, Mr. Wallace's and Mr. Cunningham's fear is groundless.

Mr. Wallace's and Mr. Cunningham's interpretation that the author intends the protective device to furnish equipment isolation only is based on the statement that "The protective device will function only when the equipment is worthless." However, if this statement is coupled with the sentence immediately preceding it in the paper: "It will operate, protecting the equipment, whenever the temperature of the hot spot exceeds a safe predetermined temperature" the fallacy of Mr. Wallace's and Mr. Cunningham's conclusions becomes apparent. The protective device as described in the paper will, as they state, give equipment isolation, but at the same time it also will give equipment protection.

Mr. Wallace and Mr. Cunningham have followed the usual, but erroneous, line of thought in the consideration of the weight of the fuse. As soon as the thermally operable current interrupter is eliminated as they propose, the "short-circuiting strip" must take up the burden of opening safely over the entire range of available currents. Then the strip no longer can be designed with the sole function of protecting against short circuits, but instead its design must be modified by its performance at the lower currents. This in turn drags the case size and link size up to those given for the straight-through fuse as illustrated in the paper.

The question of the magnitude of the fault currents in distribution systems is debatable, and the true answer will be obtained only from actual field experience. However, the larger the electric load of the airplane grows the more closely the electric system approaches the low-voltage power system, and we know from field experience in this application that the time-lag fuse actually protects feeders from cooking out on a long-continued light overload caused by a light ground or by the connected load's increasing beyond a safe value.

Mr. Boice, like Mr. Wallace and Mr. Cunningham, has misinterpreted the purpose of the protective device as explained in the paper. He assumes that the protective device will give equipment isolation only instead of equipment protection. However, the paper states that "it will operate protecting the equipment, whenever the temperature of the hot spot exceeds the safe, predetermined, temperature." From this statement it is apparent that the protective device not only will isolate the equipment, but also it will protect.

The point Mr. Boice brings out that, as the aircraft supply is changed from direct current to 400-cycle alternating current, the maximum short-circuit current may decrease instead of increase, further accents the desirability of the protective device outlined. Since the maximum short-circuit currents decrease, it is also reasonable to assume that it will be possible to obtain lower fault current than the maximum, and some of these currents may be low enough to cause the limiter to burn up, destroying itself as well as the associated equipment. However, the time-lag fuse will handle all magnitudes of overloads satisfactorily, thereby assuring satisfactory performance.

The author cannot agree with Mr. Boice in that grouping of the circuit protectors will assist in locating faults. The closer the protector can be placed to the point at which the fault has occurred, the simpler will be the circuit-checking job. Such a system would provide convenient test points, which would aid materially in buzzing out the circuit. As already mentioned, the grouping of protectors in one location also increases the vulnerability of the circuit in military aircraft.

The author heartily concurs with Mr. Boice's opinion that "careful study of each fuse application will be well repaid in terms of improved protection." If such a procedure is followed, the protective devices will truly live up to their name and will perform the function for which they are designed.

Mr. Powell's conclusion that a fuse with a high-temperature melting link is most suitable for maintaining the continuity of service in aircraft circuits is not justified by the facts in the case. With such links it is possible to have a low overload of damaging proportions, which will be below the fusing point of the high-temperature melting links. Under such conditions the voltage drop will become excessive, and the load on the generating equipment unusually heavy, so disturbing the whole system. Such a load definitely should be removed, and it only can be removed by protective devices as outlined in the paper.

The argument that the low-temperature melting link is not suitable because the effect of ambient temperature on it is not reasonable, if the low-temperature link is used in conjunction with a high-temperature link as outlined in the paper. The variation in ambient temperature will affect the capacity of the circuits, and for complete protection the protective device should be affected in the same manner.

Mr. Powell's contention that there is no distinction between the words limiter and fuse is true from a theoretical standpoint. However, the word "limiter" has been associated, as Mr. Powell states, with the "reduced section link suspended in air and connected in cables of network systems to provide a point at which the circuit may be interrupted, when the system develops a fault." Also, the term limiter has been associated with those aircraft fuses which are used merely for short-circuit protection. They are made of a high-melting-point link and the manufacturers frankly admit that on some loads the link itself will reach damaging temperatures, thereby destroying itself as well as the associated equipment. However, they justify the existence of such a fuse or limiter by assuming that these values of current will not be realized in the application and need not be considered in the design of the link. This, though greatly simplifying the design of the fuse itself, does not seem reasonable. Every designer appreciates that, no matter how severe the laboratory test may be, field conditions invariably find a point which has not been considered. Therefore, the protective device to justify its name should be so perfect that it will take care of all the unforeseen conditions and function to protect the circuit wiring as well as equipment no matter what condition may arise.

The large number of people who have discussed this paper seems to indicate that the subject of electrical protection for aircraft has not been given the clean-cut basic consideration that it deserves. This fact is further emphasized by the conflicting opinions expressed by the various discussers. In fact, almost every argument which has been brought forward has found its counterpart in a criticism offered by some other person in his discussion. The author well realizes that each and every one of these men are more than qualified to discuss the problem intelligently, so that the conflict of opinion only can be attributed to the fact that the fundamentals of the case have not been crystallized well enough to permit a complete solution as yet. The author has the utmost confidence in the ability of the time-lag fuse outlined to give the protection which he feels the electric circuits of the airplane justly deserve. However, this type of protection will not be obtained as long as the general attitude is to consider the protection after all of the other details have been worked out and then to compromise the design to meet conditions resulting primarily from the limitations imposed on it by the designers of the other component parts.

The Measurement of Lightning Currents in Direct Strokes

Discussion and author's closure of paper 44-114 by G. D. McCann, presented at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944, and published in AIEE TRANSACTIONS, 1944, pages 1157–64.

E. Beck (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The growth of statistical data, as accumulated in the widespread investigation described by Mr. McCann, provides more and more reliable information on what we may expect of lightning strokes. In fact, it is now possible to make valid estimates of the probability of lighting damage. The interpretation of these stroke data in terms of the performance of protective devices such as lightning arresters will require some further clarification and relation to similar data on the lightning currents discharged by arresters in service. At the University of Pittsburgh, one of the stroke-current measuring stations, types of lightning arresters have been connected in the stroke circuit during the several years covered by the study. Certain strokes have been discharged without harm to the arresters, but in other cases some or all of the arresters have been damaged. The results correlate in general with laboratory tests. One would expect that, if arresters of any available type were called upon to discharge with appreciable frequency currents such as are measured in direct strokes, the mortality of arresters in service would be considerably higher than it is. This has been pointed out before in connection with some earlier papers in which it was indicated that the severity of the arrester-discharge currents is in general considerably less than the stroke currents. An interesting point in this connection has been made by Mr. McCann to the effect that the major part of the total charge in the lightning discharge occurs during the flow of the low-magnitude continuing current and that the high-current component, although it may reach a crest of 100,000 amperes or more, hardly involves more than a few coulombs. Apparently, arresters on systems usually are spared the necessity of discharging all of the low-current high-charge component. This results from the facts that system flashovers sometimes are caused by the high-current component and provide a discharge path separate from the arrester and the transformer windings on grounded neutral systems provide a drainage path for the long-duration component. There is still another consideration that enters into this problem. During the flow of the low-current component, the voltage produced by the lightning surge and the normal system voltage are of more or less comparable values. The actual voltage at the lightning-arrester terminals then will be a combination of these two, and, when this resultant voltage passes through zero, the lightning arrester will become insulating and will not restrike unless the resultant voltage later exceeds its spark-over value. Such conditions can be set up in the laboratory, and they no doubt work in favor of the lightning arresters in the field. Although we can say, based on the University of Pittsburgh experience, that there are arresters that will withstand some types of direct strokes, it cannot be said yet that there is any arrester that will withstand all types of direct strokes. Such a requirement seems hardly necessary, since the rate at which the modern arresters are damaged by lightning throughout the country is a very small fraction of a per cent per year.

It is of interest to note that information on the râte of rise of lightning currents also is beginning to accumulate. This is an important factor in the protection of electric apparatus, and it is to be hoped that the investigation of this aspect of the problem will continue.

J. H. Hagenguth (General Electric Company, Pittsfield, Mass.): Doctor McCann's paper is a helpful contribution to the statistical evaluation of lightning-stroke characteristics. It is remarkable how close these stroke data check with the Empire State Building[1] results reported in 1941.

Differences should be expected, in particular, with respect to the initiation of the stroke mechanism, as has been pointed out in the past. However, Table I of this discussion shows that the differences are sufficiently small to conclude that the Empire State Building results are applicable to buildings of lesser height, as far as the overall direct-stroke picture is concerned. The principal differences occur with respect to the long-duration component—total charge and total duration. This is a result of the difference in initiation of the stroke mechanism as has been pointed out in the past. Consequently, a larger number of strokes to the Empire State Building are of the continuing variety only, with relatively long duration and charge. The rate of rise and wave front show differences which may be attributed to differences in measuring equipment. The cathode-ray oscillograph with an accurate shunt should give the most reliable data. It would be interesting to know how laboratory calibration of the magnetic surge-front recorder checks the oscillograph measurements, and, in particular, what is its lower limit of recording.

In any measurements on a single building, the stroke-current mechanism will be somewhat different from what it would be on a transmission line. In the building the stroke current is collected through its own ground resistance, whereas on a line current is collected over ground wires or line wires over a considerable area, connected together by metallic conductors. In this respect, the Empire State Building represents the stroke to a line perhaps better than a fire tower on top of a hill, because the building's ground connections are very extensive through the maze of water, gas, and other service pipes in New York.

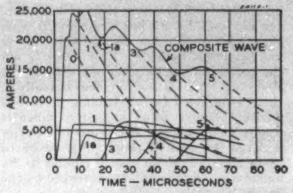

Figure 1

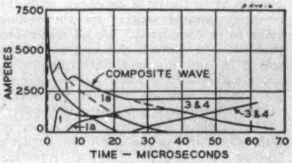

Figure 2

With respect to continuity of current flow, record 113, Figure 6 of the paper seems to give definite indication that current below 0.1 ampere did not flow. On the other hand, record 141 of the same figure shows more illumination between current peaks than record 123, and yet Doctor McCann concluded that current flow between peaks did not exist. Is it possible that the smaller aperture of the pinhole may introduce sufficient error so that small currents cannot be read at all times? The great difference in recordable light sensitivity produced by aperture clearly is shown with the multiple-aperture camera.[2] It would seem that the success of recording small currents would depend to a considerable degree on the state of incandescence of the gap electrodes of the photographic surge recorder and, therefore, on the previous history of current flow through the gap. Perhaps a sufficient number of coulombs have to flow through the gap before a photograph of a 0.1- or 0.2-ampere current can be obtained. Although it is probably not of particular practical importance whether a continuous current always connects current peaks, it seems quite impor-

Table I

Per Cent of Strokes Exceeding Value Given in Succeeding Columns	Total Duration (Seconds)		Total Coulombs		Current Peaks (Kiloamperes)		Wave Front (μs)		Effective Rate of Rise (Kiloamperes/μs)		Current Peak to Half Value (μs)	
	Empire State	Paper	Empire State	Paper	Empire State	Paper	Empire State	Paper	Empire State	Paper	Empire State	Paper
50	0.3	0.08	25	9	9	5.5	1	2.5	15	9	31	42
20	0.5	0.3	63	39	15	15	3	4.2	28	17	47	58
10	0.7	0.5	100	88	25	25	5.5	5.5	35	35	80	70
Maxima	1.6	1.3	165	100	58	160	6	10	35	45	115	90

tant from the theoretical point of view.

It seems to me that the photographic surge recorder has not proved necessarily that small currents do not flow. It would seem wise to reserve final conclusions, until conclusive proof can be obtained.

With regard to high-frequency fluctuations of high-current peaks, Doctor McCann explains these with successive tapping of branches during the progress of the return streamer toward the cloud. Though this explanation is very intriguing, it would not apply to the Empire State Building where downward branches rarely are photographed, particularly on the second and third current peaks. Similarly, photographs by Schonland and others rarely show branches on other than the first current peak. In spite of this, pronounced oscillations were observed on all current peaks measured at the Empire State Building with the frequency of oscillations of a very regular nature. Though it is difficult to see that each stroke has the same characteristics, it is quite possible that these oscillations are caused by charge distributions in the ground system.

The oscillations on this basis then can be explained as produced by the front and the tail of the different component waves arriving from various portions of the charge centers of the ground system of the Empire State Building (zone 0), such as the base of the building (zone 1), nearby tall buildings (zone 1a), East River (zone 2), Hudson River (zone 3), and more distant points. This concept is shown on Figures 1 and 2 of this discussion. It is interesting to note that the type of wave shown on Figure 1 occurs during the first current peak. If the interval between first and second current peak be long (0.05 second or more), the second peak will have a shape similar to the first. If the interval be short (0.01 second), a wave similar to Figure 2 results. Of course there is never an exact reproduction. However, the time of current minima indicating the arrival of new charges reoccurs with great regularity. In many waves some of the component waves are missing indicating that no charges were available from these centers. Although this is only an assumption and has not been proved, it seems that it explains very well the current phenomenon at the Empire State Building. Perhaps at the University of Pittsburgh similar ground conditions exist. I always have been puzzled by Malan's and Collens' photographs which seemed to indicate to me that the branches and cloud discharge into the ground contrary to the explanation given by them that the ground feeds into the branches. Current flow to ground is expected, when strokes occur to very high resistivity soil.

I do not believe that the length of the tail of the current waves is a factor which will greatly influence co-ordination, since it is the voltage wave which will cause spark-over. The voltage wave, however, is not a duplicate of the current wave but is influenced by such factors as ground impedance and the multiplicity of paths taken by the current. Only as a traveling wave are currents and voltages of the same shape. For these reasons, a long-tail current wave at a given point in a system rarely will result in a long-tailed voltage wave elsewhere in the system.

It is hoped that it will be possible in the near future to present photographic lightning data obtained in Pittsfield and New York to buildings or places other than the Empire State Building which have been obtained during the years, 1935–41.

References

1. LIGHTNING TO THE EMPIRE STATE BUILDING, K. B. McEachron. AIEE TRANSACTIONS, volume 60, 1941, September section, pages 885–90.

2. LIGHTNING, J. W. Flowers. *General Electric Review*, April 1944, pages 9–15.

K. B. McEachron (General Electric Company, Pittsfield, Mass.): When the investigation of lightning to the Empire State Building was begun in 1935, it was recognized that the great height of the building might have a pronounced effect upon the character of the lightning discharge. It was decided therefore to carry on a similar investigation in Pittsfield to study lightning to ordinary terrain, and to this end an observatory was erected upon the roof of one of the highest buildings in the Pittsfield plant of the General Electric Company.

The investigation in New York made use for the first time of oscillographs designed to record the wave shape of individual current peaks and also to record the variations of current over times as long as one second, these results to be correlated with rotating lens or film cameras also designed for corresponding high- and low-speed operation.

Cameras of similar character were used in New York and in Pittsfield, the reason being that correlation might be effected through the photographs obtained to high buildings in New York and to ordinary terrain in Pittsfield. Since the correlation was to be through the medium of the photographic film, a calibration of each film was essential, and, therefore, each film used in New York and Pittsfield was given calibration through the use of a constant light source and a series of neutral filters as described by J. W. Flowers.[1] In New York and in the laboratory, the lightning photographs would be calibrated against the current measured oscillographically, and thus some measure of the Pittsfield stroke currents would result. A microdensitometer was developed to measure the density of the image on the photographic film.

A large amount of data have been accumulated, some of which have been published, but much remains which has not been put in shape yet for publication. The necessity of using man power on more urgent work in connection with the war stopped further investigation at the end of the 1941 lightning season.

With recognition of the limitations of the photographic film, a camera with four identical lenses was developed,[1] which was arranged with various apertures and filters giving an effective f range from 4.5 to 1,620. The results from this camera showed that many of the photographs taken with the previously used cameras, which did not show luminosity between current peaks, would have shown such effects had the wider exposure range of the multiple-aperture camera been available. I cannot help feeling that Doctor McCann would have found evidence of continuing currents, if the conditions had been more favorable for recording it photographically. Distance and visibility have much to do with the answer.

As for the difference between the Empire State Building and the University of Pittsburgh, it does not seem likely that the difference in building height can have much to do with stroke characteristics, once the stroke path has been formed. One might expect that with the 1,275-foot structure the cloud charge before the stroke would be less than for the 585-foot building, particularly when the stroke is initiated by the downward leader, although the data of Figure 12 of the paper seem to show the reverse.

It is difficult for me to reconcile the proposition that sometimes the continuing current is present and other times it is absent. Certain conditions must be met so that new channels will not be formed as the result of cessation of the current, if the stroke is to be a true multiple stroke throughout its length. Thus, in 1936[2] a stroke consisting of at least four successive current peaks made initial contact to a building 360 feet high and 1,200 feet west of the Empire State Building. This stroke was initiated by a downward step-leader. After the first current peak, the stroke shifted from a point 655 feet above the top of the Empire State Building in such a manner that the three succeeding current peaks made contact with the top of the Empire State Building. The first one of these three succeeding current peaks showed evidence of downward streamers indicating the step-leader mechanism between at least the point 655 feet above the building and the top of the building. Similar changes of path had been observed by others, and by ourselves in other locations.

The reasons for such a change are not clear, since it would be expected that the path already travelled by the stroke presumably will be a better path than would be a new one which had not as yet been formed. It would appear from these phenomena that there are cases at least where the original path loses its conductivity to such a degree that strokes do shift their path, whereas other parts of the same stroke remain sufficiently conducting so that the new current peaks remain in the old path. It is unfortunate in the case of this particular stroke to the Empire State Building that we have only the photograph with the still lens, and therefore the time interval during the change from one path to another is not known.

Laboratory tests reported in 1939[2] appear to indicate that, when the current ceases, the luminosity ceases also. Later, tests by Flowers[2] seem to substantiate this contention. Our unpublished data appear to indicate that in the cases where the stroke was near enough to the Pittsfield Observatory and the visibility good, illumination was recorded between the current peaks. It seems to me, therefore, that more data are needed before the proposition that current peaks are not tied together by continuing current is acceptable.

References

1. LIGHTNING, J. W. Flowers. *General Electric Review*, volume 47, number 2, April 1944, pages 9–15.

2. LIGHTNING TO THE EMPIRE STATE BUILDING, K. B. McEachron. *Journal* of the Franklin Institute, volume 227, number 2, February 1939, pages 149–217.

3. The Channel of the Spark Discharge, J. W. Flowers. *The Physical Review*, volume 64, numbers 7 and 8, October 1 and 15, 1944, pages 225–35.

G. D. McCann: As pointed out by Mr. Beck lightning arresters are seldom called upon to discharge more than a small fraction of the total energy in a direct stroke. One of the principal causes for this is that flashover usually occurs at some point on the power system before very much of the stroke energy has passed through the arresters. The preponderance of the energy is contained in the low-magnitude continuing portion of the discharge which passes through the flashover path rather than through the arrester, since the arrester offers a considerably greater impedance to the flow of such low-magnitude currents. The effect of the character of transmission- and distribution-line insulation and system grounding upon this factor is being studied in the Westinghouse lightning investigation. It is hoped to treat this more fully at some future time.

Mr. Hagenguth points out that differences in the character of strokes to such tall objects as the Empire State Building as compared to structures of more normal height might be expected. He, however, asserts that comparison of such data shows that these differences "are sufficiently small to conclude that the Empire State results are applicable to buildings of lesser height as far as the over-all direct-stroke picture is concerned." The data of this paper show that there are important differences in the fundamental characteristics of the stroke mechanism and the wave shape of the discharge current (see Figures 3, 11, 12, 15, and 16). These differences show quite clearly that for the practical problem of determining the lightning performance of power systems, only data obtained on structures of lower and more normal heights should be used.

Mr. Hagenguth asks about the accuracy of the magnetic surge-front recorder as compared with the cathode-ray oscillograph in recording the front of current waves. Laboratory calibrations[1] show that this instrument should record the effective fronts that exceed one-half microsecond to an accuracy of 10 or 15 per cent, except for certain types of multiple strokes. In all such cases where a record cannot be obtained, however, the effect of successive high rates of current rise in the multiple strokes can be detected and the data ruled out. To date some five direct strokes and 32 lightning-arrester discharge currents have been simultaneously recorded with the surge-front recorders and the cathode-ray oscillograph. The maximum difference in the effective front as measured by the two instruments has been 20 per cent.

Mr. Hagenguth also points out the possibility of fluctuations on the tail of current waves for strokes to the Empire State Building, which are produced by reflections from points of discontinuity in the ground path of the building. Such an effect seems quite plausible and very interesting. Apparently, however, it causes relatively small-magnitude fluctuations as compared to the greater ones possible from those described in this paper that result from the prominent branches that can occur in strokes to open ground or to objects much lower than the Empire State Building.

Both Mr. Hagenguth and Doctor McEachron question the reliability of the photographic surge-current recorder in measuring the low-magnitude continuing currents, since records obtained with this instrument refute their contention that continuing current should flow between components of multiple strokes. This instrument has been calibrated carefully in the laboratory for a wide range of discharge-current conditions, and it will record reliably all currents in excess of 0.1 to 0.2 of an ampere. Contrary to Mr. Hagenguth's belief, only the photographic intensity of the arc between two electrodes is recorded with the instrument and not the luminosity of the electrodes themselves which is blocked off by a set of buriers. This device does not have the limitations in recording that result when attempts are made to photograph the actual stroke channel at a relatively great distance and very great variations in visibility are produced by intervening clouds.

Doctor McEachron finds it difficult to reconcile the proposition that successive discharges in a multiple stroke can travel down essentially the same stroke path without a continuous flow of current in that path. To those familiar with the mechanism of conduction in gases, the principal consideration governing the maximum time interval that can exist after current has died out in an arc path and a successive application of voltage will result in a discharge occurring in essentially the same path is determined by the rate of dielectric recovery of the arc column. Studies of long arcs produced in the laboratory[2] have shown that even for discharges of high magnitude and short duration that are not followed by continuing low-magnitude components of very great duration, the rates of dielectric recovery of the arc path can be slow enough for time intervals of the same order of magnitude as those commonly occurring between components of multiple strokes.

References

1. New Instruments for Recording Lightning Currents, C. F. Wagner, G. D. McCann. AIEE Transactions, volume 59, 1940, pages 1061–8.

2. Dielectric-Recovery Characteristics of Large Air Gaps, G. D. McCann, J. J. Clark. AIEE Transactions, volume 62, 1943, January 1943, pages 45–52.

Ring Busses for Generating and Transmission Stations

Discussion and authors' closure of paper 44-116 by E. L. Michelson and J. P. Galassini, presented at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944, and published in AIEE TRANSACTIONS, 1944, September section, pages 665–9.

R. L. Webb (Consolidated Edison Company of New York, Inc., New York, N. Y.): This paper on bus design is indeed timely. Operators would find it advantageous to study bus layouts which are described here and which have been described in many other papers during the last five or six years to assist with new planning and modernization programs, which certainly will have to be faced within a few years.

The authors have given some history of bus-layout development on the several voltage systems used in Chicago. They have also described features of reliability, flexibility and protection which are of interest. Very little has been said about cost. It would be interesting if they could supplement the paper in their closure with information which would assist the reader in regard to comparative costs of the various bus schemes described and illustrated.

The paper spends little space in discussing other features of bus design which we consider extremely important. These consist of:

1. Physical isolation of major bus sections.
2. Use of fire walls where good physical isolation cannot be obtained otherwise.
3. Use of double breaks in tie circuits between isolated major bus sections.
4. Ventilating system design for switch houses, which will prevent intercommunication by gases between major bus sections.

A paper was presented in 1939 to describe recent switch-house designs used for generating stations in New York City.[1] It will be noted by referring to Figures 2, 12, and 22 that the company has favored a star bus arrangement, sometimes referred to as a "syn bus" station in recent modernizations. The syn bus consists essentially of a bus that may be sectionalized by breakers, which serves as the synchronizing connection for several major bus sections. These major bus sections are supplied by direct generator connections and serve their respective loads through distribution feeders connected to feeder group busses or to feeder stub busses. The generators and their respective bus sections which form the points of the star arrangement are synchronized through reactors at the syn bus.

It has been found that star bus arrangements serve satisfactorily in regard to flexibility, minimum short-circuit duties on circuit breakers, reliability of supply to the distribution circuits, and switch-house costs. As a matter of comparison, the arrangement shown in Figure 22 of Mr. de Bellis' paper makes use of less than half the circuit breakers required in another station on the system having less than 80 per cent of the generating capacity. Though such a comparison does not bear a direct relation to cost, it is an index of the tremendous savings which can be realized by careful attention to all of the important features of bus design for generating stations.

Reference

1. Modernization of Switch-House Design, Consolidated Edison Company of New York, Inc., A. M. de Bellis. AIEE Transactions, volume 58, 1939, pages 752–60.

E. L. Michelson and J. P. Galassini: The authors wish to thank Mr. R. L. Webb for his comments on their paper. No attempt has been made by the authors to obtain the cost figures for the various bus layouts described in the paper; however, experience has indicated that the cost of these bus layouts will vary somewhat in proportion to the number of breakers used in the installation. The cost of other items, such as control and relaying equipment, is approximately the same regardless of the type of bus arrangement used.

Very little reference has been made to the physical design of the busses because this paper was written primarily from the viewpoint of the engineer who plans the electric connections of a bus rather than for the engineer who is concerned about the physical design; however, in answer to Mr. Webb's questions, the following is a brief description pertaining to the physical aspect of our most recent bus installation shown on Figure 3 of the paper.

The major bus sections, which make up this ring bus, are physically isolated from each other. Each bus section is installed in a separate room with a passageway between the rooms. The rooms have no windows and their steel doors are always kept closed. Each room has a separate ventilating system and also an emergency smoke-exhaust fan. No fire-extinguishing equipment is installed because of the use of oil-poor breakers. Two bus-section-tie breakers connected in series are used to tie adjacent bus sections. Each of the bus-section-tie breakers is installed in a separate room in order to obtain physical isolation between them. The two main rings are installed on opposite ends of the switch house and are separated by a distance of approximately 80 feet.

Transmission-Line Electric Loadings

Discussion and author's closure of paper 44-115 by S. B. Crary, presented at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944, and published in AIEE TRANSACTIONS, 1944, pages 1198–1204.

H. P. St. Clair (American Gas and Electric Service Corporation, New York, N. Y.): The author of this paper is to be commended for presenting a clear picture of the reactive kilovolt-ampere or kilovar requirements and losses involved in getting power over transmission lines. Real power losses, I^2R values, are recognized universally, but the full significance of I^2X losses is not so generally understood. Perhaps one reason for this lies in the author's observation that with few exceptions loadings for lines of 100 miles or over have not much exceeded the 2.5-kv² or unity-power-factor value, so that I^2X losses have been taken care of by line-charging kilovolt-amperes. Figure 4 of the paper shows how the net values of kilovar loss cross the zero line at about 2.5 kv².

This same curve, however, also shows the rapid building up of I^2X loss at higher loadings, up to 5 and 7.5 kv², which the author suggests can be attained economically by providing the necessary supply of kilovar capacity. For example, referring also to Table II of the paper, these net losses on a 132-kv line would be zero kilovar at 44,000 kw, 35,000 kvar at 88,000 kw, and 99,000 kvar at 132,000 kw. It is true that these high loadings can be reached, assuming adequate conductor sizes, and it may appear to be good economics to install the kilovar capacity to reach them. From a practical operating standpoint, however, it is important to have some extra margin of line capacity available for emergencies, and, when lines are loaded in this manner, at 5 kv² and higher, there is little if any "stretch" left. It is obvious from the shape of the curves of Figure 4 that each increment of synchronous-condenser capacity is going to be added at a rapidly diminishing return, and at each higher level of loading reached the line will have a decreasing margin for additional loading. We actually have experienced this sort of thing on our 132-kv lines between Ohio and Indiana, where steadily increasing war loads have necessitated increased loadings on these lines. In this case the recent installation of a fairly large synchronous condenser, although taking care of a local voltage problem, seemed to provide a relatively small increase in the capacity of the already heavily loaded lines. At lower levels of loading the same condenser would have provided several times the capacity increase that was actually obtained. It also may be of interest to point out that the installation of a new line providing a third circuit, closing a loop between Ohio and Indiana, will reduce the loading on the existing circuits to the point where kilovar requirements of the existing systems will be reduced by 75,000 to 100,000 kva.

What I should like to emphasize, therefore, is that along with the favorable economics of heavier loadings there are certain operating disadvantages, such as loss of marginal capacity, which must be considered carefully in studying any transmission problem. On this basis I question whether it would be sound practice in very many cases to load 100-mile lines, or even shorter sections of lines in a long system, to anything higher than about 5 kv² for normal load conditions. It would be sufficiently difficult, even at this loading, to provide for the extra capacity needed in an emergency.

Although conductor size has a relatively small effect on reactance and I^2X losses, it does permit substantial control of power losses. If a large enough conductor is used, the I^2R losses attending transmission loadings of 5 kv² and above can be held down. Here again, however, the curves are getting steep, and practical considerations must be added to the theoretical economics. Cost of lines may increase more rapidly above certain conductor sizes.

In general, however, I believe the author's plea for consideration of more generous conductor sizes in transmission-line planning may well be heeded. Most of us have had many occasions to wish that we had installed larger conductors on some given line, both from the standpoint of losses, and of line capacity. The relative performance and kilovar requirements of lines with varying X/R ratios shown in the paper indicate the strong desirability of using ratios closer to 0.2 than to 0.4. On this basis a conductor of 336,400 circular mils aluminum conductor, steel reinforced with a ratio of 0.35 is definitely small, whereas ones of 477,000 or 556,500 circular mils, with ratios of approximately 0.25 and 0.21, respectively, are much to be preferred.

One factor seems to have been overlooked by the author in his comparison of a 300-mile and a 100-mile line, both loaded to the same stability limit. It is shown that with the same conductor size (or with only a slight increase) the over-all percentage loss on the 100-mile line carrying three times the load would be no greater than that on the 300-mile line, even though the actual loss per mile is nine times as great. In this situation it would seem that good economics would not permit the same percentage loss on a 100-mile line that would be considered reasonable on a 300-mile line. In other words there should be some relation between the acceptable losses and the relative size of the line investment producing those losses. If the comparison made by the author is carried down to still shorter line lengths, unreasonable results immediately become evident.

Again, I wish to commend and thank the author for the presentation of this paper in which, I believe, he has made a substantial contribution to a better understanding and a clearer perspective in viewing our present and future transmission practice.

C. F. Wagner (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Mr. Crary refers variously to the load absorbed by a resistance equal to the surge impedance of the transmission line as the "natural loading," the "surge-impedance loading," or the "unity-power-factor loading." I wish to comment briefly upon the significance of this quantity.

When a resistance equal to the surge impedance of the line is connected across the receiving end of a resistanceless line, a surge introduced into the sending end is absorbed completely without reflection. Thus a sinusoidal voltage introduced into the sending end, travels along the line and is completely absorbed. The voltage at the receiving end varies sinusoidally with time, has the same magnitude as the voltage at the sending end, and is displaced in time by an angle equivalent to that required for the wave to move from one end of the line to the other. Since electric waves propagate at the speed of 186,000 miles per second, in one-quarter cycle of a 60-cycle circuit a wave would travel 186,000/(4 × 60) or 775 miles. Thus, the voltages of a 775-mile line would be displaced 90 degrees, a 300-mile line 35 degrees, and other lines proportionately. These relations can be used to plot the receiving and sending power circles for resistanceless lines. This has been done in Figure

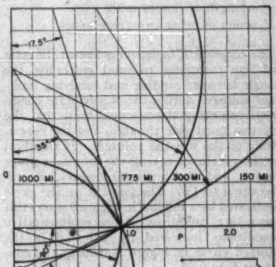

Figure 1. Power circle diagram for receiving end of resistanceless lines of different lengths

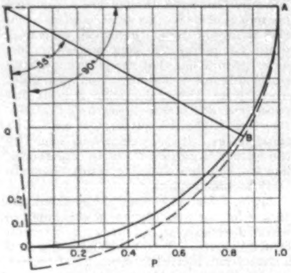

Figure 2. Evaluation of K

1. The unit of real and reactive power has been taken equal to the surge impedance loading, and all the circles have been made to pass through unit power and zero reactive power. If the resistor is replaced by a synchronous motor of zero impedance whose excitation is adjusted so that the load is unity power factor, it is quite evident that surge-impedance loading has no relation whatever to the actual amount of load that can be carried over the line under stable conditions. From a stability point of view the surge-impedance loading has no significance. Mr. Crary recognizes this fact in Figure 1 of the paper in which the upper curve shows the approximate effect of the length of the line upon the power that can be transmitted with equal stability margins.

For comparative performance of straightaway transmission systems, or for estimating loads such lines can carry, the following may be found useful. If 0.8-ohm reactance per mile is assumed,

$$P = K\frac{E^2}{0.8\,L + X_t} \tag{1}$$

in which

P = power per circuit at receiving end of line in megawatts
E = line-to-line voltage at both ends in kilovolts
L = length of line in miles
X_t = sum of other series reactances

The constant K is dependent upon the ratio of line resistance to total reactance, the angle between sending and receiving internal voltages during the steady-state condition preceding the assumed fault that the system must ride through, and the magnitude of the internal voltages. If the total series resistance is zero, the internal voltage unity, and the angle 90 degrees, K will be equal to 1.0. The point A on the circle indicated by a full line in Figure 2 shows this operating condition. More representative conditions indicated by point B in the dotted circle in Figure 2 reduces the constant K to 0.85.

The series reactance X_t is

$$X_t \text{ in ohms} = X_{(\text{per unit})}\ \frac{E^2}{MVA}$$

$$\text{or approximately} = X_{(\text{per unit})}\frac{E^2}{P} \tag{2}$$

When equation 2 is substituted in equation 1 there results

$$P = 1.25\ (K - X_t)\ \frac{E^2}{L} \tag{3}$$

If the transmission system feeds into an infinite system and

X of receiving-end transformer = 0.13
X of sending-end transformer = 0.13
X_d' of generator = 0.31

then

$$X_t = 0.13 + 0.13 + 0.31 = 0.57$$

and

$$P = 1.25(0.85 - 0.57)\,\frac{E^2}{L}$$

$$= 0.35\,\frac{E^2}{L} \tag{4}$$

With the data provided by reference 2 of Mr. Crary's paper, the quantity $P\ L/E^2$ (number of circuits) was computed for all 60-cycle lines over 100 miles in length that had no loads taken off at intermediate points. The Boulder line of the Metropolitan Water District of Southern California was omitted because the data in the table did not appear consistent. The results of the calculations give an average of 0.32 for this quantity with a maximum for the nine values of 0.43 and a minimum of 0.17. The larger values probably represent systems that operate closer to their steady-state stability limits or that have lower values of series reactance external to the line. Conversely the lower values are lower for the opposite reasons. The results indicate a substantial check for such an approximate relation. Slightly greater refinement can be attained by using equation 3 with K set equal to 0.8 or 0.85 and X_t set equal to the sum of the terminal impedances. In this case the reactances for the generator may be taken as their per unit value times their power-factor rating.

J. G. Holm (Boston Port of Embarkation, Boston, Mass.): One seldom comes across a paper written with so much clarity and simplicity as this paper by Mr. Crary, and it is by no means to increase the lucidity with which the, so to say, philosophy of power transmission has been presented in the paper that these comments are offered. It is my observation, however, that there exists among some engineers a slight misconception of the transmission of the "natural" loading over a power line, which perhaps one should attempt to rectify.

The "natural", or surge-impedance, or the unity-power-factor loading as it is variously called cannot be transmitted with stability at a certain voltage over a transmission line of any length.

For example, for a system with representative values of machine reactances, transmitting power at 138 kv, and connected to an infinite bus, a "natural" load of 47,500 kw (2.5 × 138^2) may be transmitted easily with stability over a 200-mile line, with practically zero reactive power taking part in the transmission. If the line length were to increase to 950 miles with the same terminal conditions and line voltage, the transmission with stability of the "natural" load would become impossible, and, in order that 47,500 kw of real power could be transmitted, more than this amount of reactive power would have to take part in the process. In such a case, then, the concept of "natural" power may be discarded entirely, and the system designed on general stability and economic principles.

If a rotor-angle curve is analyzed for a 60-cycle representative system (machine and transformer reactances of 30 per cent) connected to an infinite bus, one will see that the "natural" loading may be transmitted with steady-state stability over a line whose length lies anywhere between the following approximate limits: zero to 625 miles, 1,400 to 2,175 miles, 2,950 to 3,725 miles, not extending the figures to lines of still greater length. In the same system the transmission of "natural" loading may be accomplished with transient stability in lines of the following approximate lengths: up to 215 miles, from 1,400 to 1,750 miles, and from 2,950 to 3,300 miles.

For other line lengths where transmission with stability of "natural" loadings is impossible at conventional frequencies with the present state of the art of the various voltage-regulating devices, or again for lines of this length transmitting greater than "natural" blocks of power, a larger or smaller amount of reactive power must be transmitted along with the transmission of real power. It is in this connection that it is important, as Mr. Crary points out, that conductors of lower resistance be used, if the maximum possible amount of real power is to be transmitted with minimum real and reactive losses. The importance of low-resistance conductors cannot be too strongly emphasized. This should be watched especially carefully in lines operating at the lower voltages, where the conductor mechanical requirements and the corona power loss allow the use of conductors of small diameters and of little copper. The engineers of the old school would say that a transmission line has been economically designed, when it uses just enough copper to allow for an efficiency of the order of 90 per cent. Whereas a properly designed modern line capable of operating with stability at high loadings will require such a low r/x ratio, that it is practically impossible to obtain for it an efficiency of less than 95 per cent, and frequently its efficiency will be as high as 98 per cent. An over-all economic study of such a system, with all the marketable power to be accounted for, will show that the additional copper used is far from wasted and heavier conductor and costlier supporting structures will result in a more economical system than had the line been designed with as little copper as possible. Such lines, then, will not have to work at the low loadings of the systems listed in reference 2 of Mr. Crary's paper and shown on his Figure 1.

Insofar as the various means for the supply of reactive power for the purposes of power transmission are concerned, it is a pleasure for me to note in Mr. Crary's paper that the synchronous condenser seems to be in a somewhat greater favor than has been the case during the last few years under the pressure of existing war conditions. It is stated in the paper that the series-capacitor compensation is advantageous for lines of 300 miles in length for stability loadings exceeding in a one-circuit line 750-kw miles per $(kv)^2$. For shorter lines and lower loadings the synchronous-condenser compensation is recommended. Considering that a loading of 750-kw-miles per $(kv)^2$ although practical is nevertheless none too low, if it is referred to a system equipped with devices for stability improvement, and that it may be rated as rather high, if referred to a one-circuit line not equipped with such devices, it may be said that at lower loadings the series compensation will have advantages only for lines exceeding 300 miles in length. This indicates that the recommended application of series capacitors is advanced to lines of somewhat greater length and higher loading than was considered good practice a couple of years ago. In addition it is noted in the paper that a series-compensated line also will require controlled shunt compensation for the control of line voltage. It seems to me, therefore, that we are approaching a time when, with the problem of critical materials nonexistent, we shall return again to the use of synchronous condensers as, for various reasons, the best-recommended practice for the sole supply of reactive power and for voltage control in lines well over 300 miles in length.

J. W. Butler (General Electric Company, Schenectady, N. Y.): During a national emergency, such as war, cost alone is no longer of primary importance, the end result being the major objective. In view of this and other things, the last three or four years have witnessed large capacitor banks installed for kilovar supply to facilitate increased loadings in existing and new lines. In fact, some of these capacitor banks have been larger than economics would justify normally, but the end result justified the expediency.

Early in the emergency this was recognized and the attention of the industry[1] was called to the possibilities of higher line loadings through the aid of kilovar supply, without the usual regard to economy. The primary thought of the moment was that of getting maximum kilowatts in the shortest time with a minimum of critical materials.

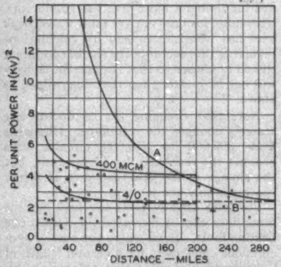

Figure 3. Economic loading of 400,000-circular-mil and 4/0 conductor, 132-kv lines, as a function of length, plotted on Figure 1 of the paper

A—Loading based on 25 per cent stability margin with representative terminal capacities, 750 – kw miles per $(kv)^2$

B—Surge-impedance loading or unity power factor

It is assumed that after the war there will be less need for saving critical materials, and economics will again be the ruling factor. Therefore, a brief speculative analysis of the probable peacetime economics of heavier line loadings and larger conductors, as suggested in this paper, may be of interest.

A particular circuit operates at its minimum annual charge per kilowatt delivered when the annual charge for the losses—kilowatt and kilovar—equals the annual charge on the total investment chargeable to the line, including the right of way. It can be shown that this loading, which is defined in this discussion as the economic loading, is independent of line length except for the effect of those terminal equipment costs that are not a function of the line kilovar loading, such as switching equipment and the terminal real estate. In order to determine the magnitude of this effect, the following case was analyzed.

1. Line—132-kv double circuit steel tower with two ground wires.

a. 4/0—$15,000 per mile

b. 400,000 circular mils—$20,000 per mile

c. Right of way—$2,000 per mile

2. Terminal equipment—Double-bus double-breaker arrangement with 3.5 million kilovolt-ampers interrupting capacity—$83,000 per terminal per circuit. Kilovar costs—six dollars per kilovar.

3. Per unit annual carrying charge on investments—0.10. Energy charges—3 miles at 5,000 hours equivalent operation.

4. The kilowatt and kilovar losses for various loadings were estimated from Figure 4 of the paper.

Figure 3 of this discussion shows the calculated economic loadings per circuit as defined above for each of these two conductor sizes. For comparative purposes, they are plotted on Figure 1 of the paper and are therefore in terms of per unit power per $(kv)^2$. These data indicate that the terminal equipment investment increases the economic loading on short lines, but its effect practically disappears beyond line lengths of 40 or 50 miles. Larger conductors have correspondingly higher economic loadings, but the general shape is the same. For the assumptions made above, these data indicate that the economic loading of a line is substantially independent of its length for a given conductor size, if its length exceeds 40 miles, and that the economic loading increases, but not greatly, as the length is made less than 40 miles. Different line costs, energy charges, per unit annual carrying charges, and so forth, obviously will affect the magnitude of these loadings, but the characteristic will be about the same.

Inquiring further into the economic picture, the annual charge per unit of power at the economic loading was determined for a 100-mile line for four voltages and three conductor sizes. Representative line and equipment costs for the various cases were used. These results are plotted in Figure 4, where it is readily seen how conductor size can be traded for voltage and vice versa. These data raise the question: Should a higher voltage or larger conductor be chosen for the larger kilowatt loads? These curves indicate that various voltages cannot be compared in terms of kw/kv^2 units, although these units do have certain analytical advantages. For instance, within the range studied the smallest annual change per kw/kv^2 is obtained with 66 kv, whereas the smallest annual charge per kilowatt is obtained with the highest voltage studied, or 154 kv. Also, the highest economic loading in $(kv)^2$ is obtained by 66 kv, but the highest economic loading in kilowatts is obtained by 154 kv. These data all bear out the axiomatic conclusion that there is an optimum economic loading for a given circuit and that this loading increases with conductor size.

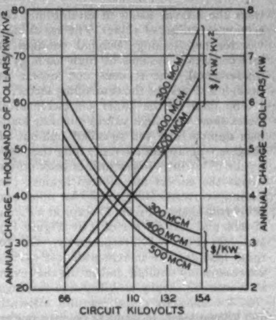

Figure 4. Comparison of annual charges at the economic loading of various 100-mile circuits for different voltages

Economic Loadings for 400,000 Circular Mils at Various Voltages

Circuit Kilovolts	$\frac{Kw}{Kv^2}$	Kw × 10^3
66	7.8	34
110	4.9	60
132	4.3	75
154	3.9	92.5

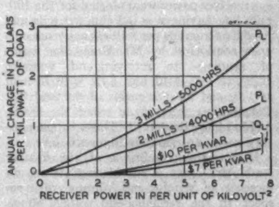

Figure 5. Annual charges for losses of a 100-mile line of $r/x=0.2$ and operated with $E_r/E_s=1.0$

Per unit annual carrying charge on kilovar-supply investment = 0.10

Figures 5 and 6 of Mr. Crary's paper give the kilovar and kilowatt losses of various 100-mile lines. To obtain an anchoring point for the relative costs of these two losses, and to determine which probably would carry the most weight at heavy loadings, the annual charges of a 100-mile $r/x=0.2$ line were determined and are shown plotted in Figure 5. These data indicate that the kilowatt losses in general should carry considerably more weight than the kilovar losses in establishing the economic loading of a line. The use of kilovar for maintaining terminal voltages of interconnected systems at high loadings is fundamental, but it does not appear as though it should be the controlling factor.

The data in the paper on incremental losses show very high values out near loadings approaching the so-called pull-out of the line. To appreciate more fully the magnitude of loadings in per unit of peak power which the curves are plotted against, the loadings corresponding to 750-kw miles per $(kv)^2$ (the locus of Figure 1) occur at a P_r/P_k of 0.60 and 0.61 for r/x of 0.10 (Figure 12) and 0.20 (Figure 13), respectively. Out to these loadings the incremental loss curves are reasonably straight, indicating that even at reasonably heavy stability line loadings (750 kw miles/kv^2), the per unit kilowatt and kilovar losses in terms of receiver power are substantially directly proportional to the delivered power.

REFERENCE

1. CAPACITORS FOR HIGHER LINE THERMAL LOADINGS, J. W. Butler. *Electrical World*, November 7, 1942, page 76.

T. W. Schroeder (General Electric Company, Schenectady, N. Y.): To observe the effects of sending- to receiving-end voltage ratios other than unity, curves supplementing Figure 4 of Mr. Crary's paper were prepared. Kilowatt and kilovar losses versus receiver power were plotted for the 100-mile line having $x = 0.8$ ohm per mile and $y = 5.2$ micromhos per mile capacity susceptance considered by Mr. Crary, for ratios of sending- to receiving-end voltages E_s/E_r of 1.05, 1.10, and 1.15, with E_r held at 1.0, and for r/x ratios of 0.2 and 0.4. Calculations were made using the transmission-line calculator.[1] These supplementary curves are shown in Figures 6 to 8.

If operating conditions at the heavier loads Mr. Crary is considering permit, an increase of sending-end voltage with respect to the receiving-end voltage allows somewhat of a reduction in real and reactive losses, particularly if the conductor is of high resistance. For example, at a loading of $5(kv)^2$, a value of E_s/E_r of 1.1 as compared with an $E_s/E_r = 1.0$, the kilovar losses are reduced 18.8 per cent or 0.40 $(kv)^2$ with an r/x ratio of 0.2 (Figure 6); whereas a similar comparison for an r/x ratio of 0.4 (Figure 7) shows a reduction of 29.6 per cent or 0.80 $(kv)^2$. At 132 kv, 5 $(kv)^2$ represents a load of approximately 87,000 kw, and these reductions amount to 7,000 and 14,000 kilovars, respectively. Similarly a power-loss reduction will obtain, and, although of a smaller magnitude as may be seen from Figure 8, it may represent a larger saving in terms of loss evaluations.

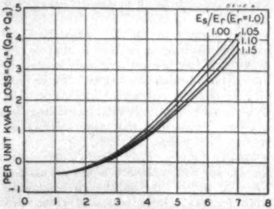

Figure 6. Reactive power loss with change in ratio of sending- to receiving-end voltage and loading for 100-mile line

$x = 0.8$ ohm per mile, $b = 5.2$ micromhos per mile, $r/x = 0.2$

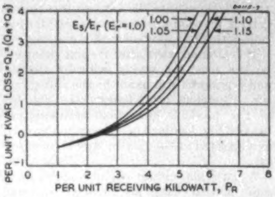

Figure 7. Same as Figure 6 except that $r/x = 0.4$

Conversely, a necessity of operating at a voltage ratio of unity increases the desirability of a low-resistance conductor.

There is a broad family of line loadings at which the line neither takes in nor puts out kilovars and at which the "equivalent transmission line power factor" referred to in the paper is unity. Mr. Crary has termed one of this family the surge impedance, unity power factor, or natural loading. At this particular loading the power factor of load transfer is essentially unity all along the line, and the difference between sending- and receiving-end voltages corresponds essentially to the IR drop of the line. At any voltage ratio other than that governed by the IR drop alone, there is a loading corresponding to the condition of zero kilovar loss in the line. At this loading the power factor, although not unity, is approximately the same along the line and depends intimately upon the voltage.

This loading decreases as the resistance increases and increases as the ratio E_s/E_r increases. Thus, for the line being considered it is about 2.1 kv^2 for $r/x = 0.4$, and $E_s/E_r = 1.0$, increasing to 2.5 for $E_s/E_r = 1.15$. With $r/x = 0.2$, it is less critical to voltage ratio, varying from 2.4 kv^2 to 2.6 kv^2 for $E_s/E_r = 1.0$ and 1.15, respectively.

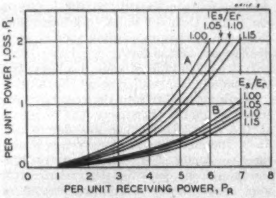

Figure 8. Real power loss with change in ratio of sending- to receiving-end voltage and loading for 100-mile line

$x = 0.8$ ohm per mile, $b = 5.2$ micromhos per mile

A. $r/x = 0.4$

B. $r/x = 0.2$

REFERENCE

1. A TRANSMISSION LINE CALCULATOR, Edith Clarke. *General Electric Review*, June 1923, pages 380–90.

Calculations were made by Mr. Shu Lee of the central-station engineering divisions, General Electric Company, Schenectady, N. Y.

H. B. Dwight (Massachusetts Institute of Technology, Cambridge, Mass.): In connection with loading transmission lines more heavily or designing them with larger conductors, which seems very opportune and desirable, the choice of conductor size by cost comparison offers a simple and reliable criterion. As is well known, if the size of conductor chosen in a design is increased until the cost of reducing the loss at peak load becomes equal to the cost of producing additional power, the point at which this occurs is given by a fixed value of current density and is substantially independent of length of line, voltage, and so forth. For high-voltage transmission lines, where the energy is supplied from the power system and off-peak power is of small importance, the load factor has little effect on the problem, which depends almost entirely on conditions at peak load.

Once the conductor size is determined in this way, it is not necessary to investigate the matters of per cent efficiency, ratio of R to X, incremental I^2R loss, or other ways of looking at the effect of line resistance, so far as specifying a larger conductor is concerned. It pays to increase the conductor size up to the point for the standard current density at peak load, and it does not pay to increase the conductor further.[1] It may be advisable to load up an existing line to a higher current density than the above value.

For copper of a cost of 25 cents per pound in place on the line, 12 per cent fixed charges, and usual values for other well-known costs, the suitable value of peak-load current density for copper is about 900 amperes per square inch. This is below the heating limit for most practical conductors. Therefore, for lines not yet built whose peak load has been determined by calculations involving the reactance the size of conductor should be chosen so that the current density will not be greater than that determined as above.

Note that changing the conductor size scarcely changes the reactance. Changing from 300,000 to 350,000 circular mils changes the resistance by 14 per cent, but the change in 60-cycle reactance at 8-foot spacing is only one per cent.

For lines already built the load current sometimes can be increased up toward the heating limit. Even where the cost of synchronous condensers for a certain length of line seems out of proportion, this may be altered, if it is found possible to install an intermediate station for load and synchronous condensers along that length of line.

The aforementioned cost-comparison method may be called an application of Kelvin's law, but, since there is a prejudice with many engineers against Kelvin's law as commonly described, it is recommended that engineers make these cost comparisons and judge them on their own merits, not allowing their judgment to be affected by prejudice.

Reference

1. Resistance Loss and Conductor Size, H. B. Dwight. *General Electric Review*, November 1938, page 484.

S. B. Crary: The discussions have all contributed very materially to a better understanding of the problem of transmission-line electrical loadings. It is particularly gratifying to obtain the comments of Mr. St. Clair, who represents both an operating and system design point of view. His comment, illustrated by an actual case, that kilowatt loading can be limited by synchronous-condenser capacity is particularly valuable. Mr. St. Clair gives an example of reduction in kilovar requirements by the closing of a transmission-system loop. The kilovar capacity saved by closing the loop will be lost, however, if the loop is opened when fully loaded. This requires making the loop transmission doubly reliable or providing sufficient kilovar capacity to meet the emergency condition due to the sudden opening. I agree with Mr. St. Clair that the percentage loss of the shorter lines should be kept down relative to the longer lines in order to keep the total transmission-line losses within reasonable limits.

Mr. Wagner shows graphically the line angular displacement for different line lengths when the surge-impedance power is being transferred. The 300-mile line, with a loading of 750-kw miles per $(\text{kv})^2$, has an angular displacement of 35 degrees, and the 150-mile line, with a loading of 375-kw miles per $(\text{kv})^2$, has an angular displacement of 17.5 degrees. Then Mr. Wagner shows by approximate formulas and representative terminal reactances, that with a total angular displacement of 55 degrees between internal voltages, the power transferred is approximately 350 miles per $(\text{kv})^2$ or in accordance with his formula, $P = 0.35\,E^2/L$. He arrived at this load transfer by using his equation 1, which neglects the increase of internal voltages with increase in loading. By using this simple approximation he arrives at this very conservative loading factor of only 0.35 or only 350-kw miles per $(\text{kv})^2$, which corresponds to a line angle of only 17 degrees. A more accurate analysis of the steady-state limits will show that it is possible to carry a loading of 750-kw miles per $(\text{kv})^2$ with a reasonable margin. This loading of 750-kw miles per $(\text{kv})^2$, of course, may apply to the period after the faulted section has been switched out subsequent to a short circuit and, therefore, would be a partial explanation for the relatively low loadings given by some of the lines plotted in Figure 1 of the paper which were based on full line capacity. There does not seem to be any justification for remaining at low loadings when full advantage now can be taken of modern design practices for improving stability and, therefore, realizing important reductions in transmission costs.

Mr. Holm has explained further the meaning of the "natural" or surge-impedance loading. He indicates that this loading may be carried up to distances beyond a half-wave length. Even if economically feasible, other difficulties can be expected to arise in the operation of these long lines such as overvoltages following short circuit and loss of load which would require compensation either shunt or series, or both. In general these line lengths appear doomed for sometime to remain only of academic interest. I appreciate Mr. Holm's comments regarding the use of kilovars for the transmission of power but cannot quite agree that this paper has shown that it will always be advisable or economical to transmit power 300 miles or greater distances straightaway by using synchronous-condenser capacity exclusively without series-capacitor compensation. Previous work and analysis have indicated that for lines of these lengths series capacitors appear altogether practical and economical as compared with the use of synchronous condensers exclusively.

Professor Dwight and Mr. Butler have supplemented the study with discussions of economics which were not attempted in the paper and are therefore much appreciated. As Mr. Butler shows, the economic loading for a given conductor size is relatively independent of the length, above about 40 miles. It is important in any economic study of this kind to realize that the loading may vary considerably from the theoretically most economic point with only a slight increase in the transmission cost. This is particularly true for the lower ratios of r/x. The load factor is probably the most important factor for determining the cost for transmission, although it has less effect in determining the best conductor size. It is agreed with Mr. Butler that all the economic factors are not very expressible in terms of kilowatts per $(\text{kv})^2$, although it was found convenient to express the loading and losses in this way. Many cases are not as ideal as those expressible in a theoretical economic study, since the transmission-line voltages to be used will depend considerably on the established level. The load factor also may be important in some cases since the kilovar capacity cost depends more on the peak capacity requirement, whereas the real losses are more dependent on the equivalent hours. Mr. Butler's Figure 5 shows a very interesting comparison between the annual charges for kilovar capacity versus losses, indicating that in general the real losses may cost more than the kilovar capacity. However, in the case of heavier loadings it would become more necessary to use a low r/x ratio, particularly for the higher-voltage lines, and the kilovar cost may exceed the cost of the real losses. If the r/x ratio is changed from 0.2 to 0.1, the ratio of annual charge for the cost of the real power losses to the reactive power losses will change from 1.73 to 0.87 for the case of real losses evaluated at 2 mills and 4,000 hours, and reactive kvar at ten dollars per kilovar at a loading of 7.5 $(\text{kv})^2$. Mr. Butler properly points out that incremental loss curves are reasonably straight up to the relative heavy loading. These curves were presented primarily to show that for an accurate evaluation of incremental losses it is not sufficient merely to take two times the total losses, and also that a distinction should be made between the incremental losses with respect to the sending or receiving end. Also, the curves point out the benefit from a loss standpoint to be gained by low-resistance conductor for a given receiver power. One of the important effects shown is the desirability of avoiding r/x ratios of greater than about 0.3 because of the decrease in pull-out power.

Professor Dwight discusses further the economic and thermal factors. The difficulty of applying Kelvin's law is that the system conditions, particularly the load to be transmitted, are not usually well known for the future system conditions. The important feature is that the cost of transmission varies only slightly from the theoretical best loading over a wider range of loads when the conductor resistance is small. This is another important advantage for the use of larger conductors and is probably more important to the average operator than the design of a line built to Kelvin's law for a given assumed loading, which may not be accurately known at the time.

Mr. Schroeder's discussion is also a valuable addition to the results presented in the paper. He shows the advantage of allowing for a voltage drop in the line in order to reduce the real and reactive power losses in terms of the receiver voltage squared. His curves show a rather large percentage reduction realized by increasing the sending-end voltage while keeping the receiving-end voltage constant. If the average or product of the sending- and receiving-end voltage had been kept constant in this comparison, the reduction shown would be reduced. Accordingly, a more accurate determination of the losses from the curves given in the paper can be obtained by taking the product of the sending- and receiving-end voltages rather than the square of either the sending- or receiving-end voltage. In any case the lowest percentage losses as well as the maximum stability for a given *maximum* line voltage are obtained with equal sending- and receiving-end voltages.

Although these discussions have brought out differences of opinion as to where the emphasis should be placed and the need for further qualification, there appears to exist a unanimity of opinion as to the advantages obtained by the use of heavier loadings, larger conductors, and controlled kilovar for reducing transmission costs.

A Method of Approach to the Consideration of Power-Distribution-System Costs

Discussion of paper 44-119 by O. B. Falls, Jr., presented at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944, and published in AIEE TRANSACTIONS, 1944, pages 1078–84.

B. M. Jones (Duquesne Light Company, Pittsburgh, Pa.): Our company has been, and is making, studies of various methods and costs of expanding its distribution system, and our conclusions match those of Mr. Falls' paper fairly closely.

We think that, where the requirements for continuity of service are not too high, individual substations of a single-unit type up to 2,000 or 3,000 kva are the most economical. We have made preliminary studies of several parts of our system and almost have completed one section at this writing. It is apparent that we will require four individual unit-type substations of 2,000-kva self-cooled transformers initially with thermostat-controlled fans for loads above this value in steps over five to ten years. The loads in this particular area have a distinct evening peak, and we believe that the gains in capacity in the 2,000-kva self-cooled unit made available by the use of thermostat-controlled fans is by far the most economical move. Hence, this points the way to the initial installation of a 2,000-kva self-cooled unit.

We have in service now a 4,000-volt distribution network, which is about 15 years old. The load of this network in 1943 amounted to approximately 3,750 kva, and it is supplied by three 1,500-kva, 22/4 kv units. This area lent itself particularly well to the installation of a network, for there were five 22-kv transmission lines going through this area, to which the substations could be connected quite economically. Actually, no transmission-line extensions or relocations were necessary. We think that the area referred to in the paragraph immediately preceding might develop into a four-kilovolt network in the future, and are exploring this further at the present time. I realize that the cost of real estate will have very little effect on the cost per kilovolt-ampere over a long-range distribution development program.

We believe that the author's paper contains considerable basic data useful to those who are studying the expansion of distribution systems, and strongly urge that they review this paper very carefully.

Capacitor Installation at Newport News

Discussion and authors' closure of paper 44-117 by V. R. Parrack and E. L. Harder, presented at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944, and published in AIEE TRANSACTIONS, 1944, pages 1165–73.

B. M. Jones (Duquesne Light Company, Pittsburgh, Pa.): This is an interesting paper, and even though we have no immediate need for such an installation, I have a few questions to ask.

Were there any special requirements for the fuse elements in the fused disconnect switches ahead of the oil circuit breakers? In reading over the paper, I gather that the current dies down so quickly (the inrush current lasts for such a short time) that the fuses are not affected. I would like some enlightenment on this.

Was there any extra duty on the oil circuit breakers from opening and closing when cutting in and cutting out the capacity banks?

How frequently were the banks switched in and out?

Did it appear practical to operate these capacitors at an unattended substation? I believe comments on this would be useful to those considering capacitors in such locations.

Reference was made in the paper to examining the bank to find a faulty unit, largely indicated by a bulge or distorted tank. In case such an inspection did not disclose the faulty unit, the bank was energized again and again until the evidence was disclosed. Now, my question has to do with the repeated energizing of the bank causing more damage—was there any disturbance on the system as a result of these repeated "shots"?

J. G. Holm (Boston Port of Embarkation, Boston, Mass.): The authors have presented a very interesting and well written paper. In view of the apparently divergent views existing on the subject of relative costs of static capacitors and the synchronous condenser for the purposes of kilovar supply,[1] it would be interesting to have the authors comment on the cost figures they gave of $7.04 per kilovar of static capacitors versus $7.93 per kilovar of the synchronous condenser based on 150 per cent of name-plate rating and 50 per cent lagging capacity. On what basis were these figures arrived at, and how was the cost of losses calculated?

If the automatic control were to be installed instead of the manual control as at Newport News, to what extent would it increase the costs?

I think the authors should be commended for not claiming for the static capacitor a resulting increase in the system-stability limits.

Reference

1. Capacitors, Condensers, and System Stability, J. W. Butler, T. W. Schroeder, W. Ridgway. AIEE Transactions, volume 63, 1944, pages 1130–8.

V. R. Parrack and **E. L. Harder:** Mr. Holm asked a question about the relative costs of static capacitors versus a corresponding size synchronous condenser. The figures were based on 15,000 kva of static capacitors versus a 15,000 kva synchronous condenser. Full-load losses on the synchronous condenser were figured at 0.0213 kw per kilovar while 0.0033 kw per kilovar was used for capacitors. An operating cycle of eight hours per day was used in the loss calculations, and the costs of losses were assumed to be subject to capitalization at 10 per cent. Estimated cost plus capitalized losses of the synchronous condenser based on its name-plate rating was $11.89 per kilovar. With the same investment and loss cost, but with an increased rating of 50 per cent due to the inherent lagging capacity, there would result 100/150 the cost per kilovar, or $7.93.

In regard to the cost of automatic control questioned by Mr. Holm, adequate control to cut in capacitors as required for regulating voltage would have cost less than 0.1 of 1 per cent of the bank cost. This was not considered necessary, since an operator was in attendance and the amount of control required was relatively small.

B. M. Jones requested information concerning fuses. The standard-type *BA* fuses selected on the basis of 60-cycle current rating and interrupting capacity were adequate for the inrush currents. The maximum inrush current obtained was 13,000 amperes dying to a negligible value in one-quarter cycle. A current of 13,000 amperes would have to be sustained for one full cycle to damage the fuse. Inrush currents under normal operation would be not over half of that obtained in the tests, since the banks are discharged before switching in normal operation. This provides a large margin of safety from blowing fuses on inrush current.

Mr. Jones inquired about extra duty on the oil circuit breakers due to opening and closing when cutting in and cutting out capacitor banks. An examination of each breaker, after about ten-months operation in cutting in and out capacitors, indicated no extra duty on the breaker contacts. A small housing which surrounded and shielded the contacts on two breakers was found to be bulged or distorted. Since the housing was useful only at near full interrupting rating of the breaker and since the breakers in this case only functioned under very light duty, the shielding housing was removed from the contacts, and operation of the breakers has continued entirely satisfactorily. The remaining three breakers, which did not have the contact housing referred to, showed no evidence of extra duty.

The capacitor banks ordinarily were switched in in the morning, out at noon, in in the afternoon, and out by approximately midnight. In addition to this normal operating cycle, one capacitor bank and breaker were switched in and out on an average of three times per day for voltage regulating purposes. These extra operations were rotated so that the breaker operations would average the same.

It would be entirely practical to operate these capacitors at an unattended station with suitable automatic control mentioned previously.

In connection with the detection of faulty units by repeated closure until a bulge is noted, Mr. Jones questioned whether any system disturbance resulted. A faulted unit being in series with several good units causes only a very small increase in the current of a bank and, consequently, has no undesirable effect, when the bank is connected to the system in the process of locating a faulted unit. Individually fused outdoor units are available also, which can be used if desired. Such were not available at the time of this installation.

Capacitors, Condensers, and System Stability

Discussion and authors' closure of paper 44-118 by J. W. Butler, T. W. Schroeder, and W. Ridgway, presented at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944, and published in AIEE TRANSACTIONS, 1944, pages 1130–8.

J. G. Holm (Boston Port of Embarkation, Boston, Mass.): The authors' paper is most welcome at this time, for it definitely clarifies one phase of the capacitor–condenser comparison, namely that existing when both of these are connected in shunt with the transmission line at either its receiving end or at an intermediate point.

However, the results obtained in the investigation are buried in the large amount of data presented, and the conclusions do not do justice to the paper in that they do not reflect the results forcefully.

Insofar as the economics of the problem are concerned, the authors state plainly that at this time the choice favors the synchronous condenser when installed in sizes somewhat below 25,000 kva.

The technical data obtained again favor the condenser practically on every point. In every case the condenser gives a larger margin of steady-state power limit than the capacitor, the margin ranging from 4 to 12 per cent. For three-phase faults at the sending end the condenser proves to be the more effective. For the composite loads the condenser characteristics are of increasingly greater relative importance. The increased inertia at the receiving end is practically neutralized by the synchronizing power increase permitted by the condenser, because of the assistance it gives the receiving end to pull out after a fault. This is not the case with the capacitor. During sudden load changes the condenser, through its synchronizing power, maintains the system voltage, automatically increasing the voltage at its terminals whenever it drops. The condenser reduces the voltage flicker which takes place with fluctuating loads. During faults and swings which follow it the condenser again maintains the system normal-frequency voltage. Connected to an intermediate point on the line, the condenser also helps to hold the voltage. None of this can be attributed to the capacitor whose characteristics are such that it rather encourages the opposite effect. When kilovars must be supplied at times and absorbed at others, a condenser may be counted upon to perform both functions; whereas a capacitor will only supply the kilovars and for their absorption a shunt reactor must be added to the system. Only the critical switching times of the capacitor have been shown by the authors to be in some cases slightly greater than those of the condenser. This difference, however, is of such small magnitude that, under the simplifications made in the investigation, it may be said to be negligible.

The point must be made clear that no advantages of any significance have been shown to exist on the side of the capacitors, on the contrary, all advantages are on the side of synchronous condensers. And to the authors must go great credit in bringing this to light, especially when one remembers that they represent a manufacturer of this equipment.

It seems to me that these results were to be expected. Just as a synchronous condenser is better than a reactor,[1] so should the condenser have advantages over a shunt-connected capacitor, considering similar flows of reactive power in the two cases. It is the condenser's ability to keep the air-gap voltage constant and its power-synchronizing ability that count, and these are not to be found in either the reactors or the capacitors.

It seems to me that the advantages of the condensers over the capacitors could have been shown to be still greater, had it not been for certain simplifications and perhaps flaws in the paper.

The three-phase fault which the authors assumed for their transient-stability case is not a very fortunate selection, not merely because this type of fault is rather rare but also because the condenser can better exhibit its synchronizing power with an unsymmetrical fault on the line. In a comparison study made by the authors this might be of significance for it could place the condenser at a disadvantage, especially when its kilovolt-amperes are selected at a rather low figure.

The condenser was represented on the network analyzer by a capacitor in series with a reactance equal to the transient reactance of the machine. The number of capacitors was regulated to maintain the voltage back of the reactance. If the capacitor were represented as a capacitance, that is, by an admittance, it seems to me that the effect obtained would be equivalent to a lengthening of the line. This would work against the condenser case, favoring the capacitor. Or am I wrong? It did not introduce any appreciable error, but I can see no reason why a phase shifter could not be used. If used, it might have counterbalanced whatever slight advantage the capacitor in this case had over the condenser.

In their conclusions the authors say that condensers at a junction point of the interconnected system do not have much effect on stability. This is an altogether too generalized statement. The authors investigated the effect of the intermediate condenser on the steady-state stability of a 150-mile 100,000-kw line operating at a voltage of 132 kv, which is certainly a low voltage for such a line. Here I am glad to note that numerically the authors' data check with my own.[1] However, intermediate condensers placed in sufficient capacities on considerably longer lines operating at higher voltages, suitably selected for the line's length, have a highly beneficial effect on the system steady-state stability limit, lifting it to a very much greater extent than that obtained on the authors' system. When as low a voltage as 132 kv is used for a 300-mile line then the potential beneficial results which may be obtained from the condenser appear in a still more unfavorable light and lead to a crippled generalized conclusion. This is why the Figure 11 of the paper, constructed apparently about the one pivotal point obtained on a 132-kv 300-mile line, leads to erroneous conclusions.

In studying their "interconnected" system the authors reduced a three-machine case to a two-machine case. Although such a reduction is perfectly permissible for combining any number of steam stations at the receiving end of the main line, it is not quite clear why the authors combined with the steam stations also the hydroelectric station beyond the switch *S* (equivalent system *E*) at the end of the tied-in line. It would be far more legitimate to consider the authors' "interconnected" system as a three-machine problem (they previously having decided to consider the synchronous condensers not as machines but as capacitors in series with transient reactances, thus eliminating two more phase shifters on the network analyzer), and it should have been studied so. A three-machine problem is simple enough to investigate on the network analyzer. For this reason it is not correct to say that the transient-stability results obtained for the "noninterconnected" system apply also to the authors' "interconnected" system, as simplified in the paper. Moreover, the validity of conclusions drawn for the "interconnected" system from Figures 16 and 17 of the paper is not convincing because of the method of reasoning applied: Swing curves obtained on the network analyzer by the point-by-point method are compared with the results obtained by the equal-area method (Figure 3*b*). Since there is a small difference in the switching times of the condenser and the capacitor cases, the different degree of exactness offered by the two methods may upset the conclusion drawn from their common use, even though the equal-area method was applied to a torque-angle curve also obtained on the network analyzer at another time.

Finally, the reasoning leading to the statement that during the longer periods of "fault on" the capacitor renders greater service than the condenser is not clear. The authors apparently say that with the capacitor on the system the longer the fault is on, the more load will be lost and therefore the easier it will be for the system to pull back. Is this not the same as saying that the longer the patient is sick, the more effective the action of the medicine will become and the easier he will come back to health? After a long duration of the fault will the machines at the system end not swing too far apart, and will the capacitor be able to bring them back to equilibrium even at a light load? The synchronous condenser, with its ability to exert greater synchronizing force resisting the separation of generators during the fault, as well as greater force tending to restore the generators after the fault is cleared, in all probability can give the system more of a lift than the capacitor, even during and after longer periods of "fault on."

Whatever is being said in the way of certain details of the study, the paper definitely is a positive accomplishment. It is suggested that the authors now take up the next phase of the problem, namely the investigation of the relative effects of the series capacitor and the intermediate synchronous condenser on the stability of transmission systems. Personally, I am inclined to believe that in this case also the over-all advantages will be on the side of the condenser.

REFERENCE

1. STABILITY STUDY OF A-C POWER-TRANSMISSION SYSTEMS, John G. Holm. AIEE TRANSACTIONS, volume 61, 1942, pages 893–905.

E. L. Michelson (Commonwealth Edison Company, Chicago, Ill.): This paper is an excellent contribution to the general subject of system stability under transient fault conditions. In the Commonwealth Edison Company, the problem of system stability as affected by static capacitors has been considered from a somewhat different aspect.

It appears that the limiting factor in determining the maximum number of capacitors that can be added to the system is generator stability. No detailed studies have been made as yet to determine the maximum number of capacitors that can be added safely to the system. A survey of the situation has indicated that it would be possible to improve the power factor up to 100 per cent under minimum-load conditions, which would result in a power factor of approximately 93 per cent under heavy-load conditions.

Because of the nature of the Chicago

system, it does not seem to be necessary to consider transient stability under fault conditions in connection with this problem. The worst type of fault against which it would be necessary to protect would be the loss of a large generator or the loss of field on a large generator.

If it is desirable to add more capacitors than are allowable considering generator stability, more capacitors could be added by use of the following corrective methods:

1. Capacitors can be switched under light-load conditions. This method has the disadvantages of additional cost and relatively complicated equipment on the overhead system.

2. Voltage regulators can be added on existing generators.

3. Reactors can be installed on generating station busses, and used under light-load conditions to increase the field of the generators. This solution does not appear to be advisable from an economic standpoint.

Any comments which the authors may care to make on this general problem will be greatly appreciated.

B. M. Jones (Duquesne Light Company, Pittsburgh, Pa.): I would like to ask the authors two questions.

First, what would be the effect of single-pole or single high-phase high-voltage switching?

As we know, single-pole switching has been discussed off and on before the Institute, and some single-phase switching is in use in several transmission systems over the country and on some tie lines between different power companies.

Secondly, on a concentrated system in a relatively small area, but one with heavy industrial loads and no long transmission lines such as the Duquesne Light system in Pittsburgh, Pa., what different effect, generally speaking, would be encountered from that shown in the author's paper along the lines of critical switching times and stability.

J. W. Butler, T. W. Schroeder, and W. Ridgway: The tentative limits of power-factor improvement on the Commonwealth Edison system indicated by E. L. Michelson appears very rational. Experience and analysis both indicate that generator power factors of unity at minimum loads are satisfactory from the stability standpoint.

The situation arising from loss of field on a large unit generally is one of low bus voltage and excessive kilovar demands. We feel this contingency can be handled best by the use of generator voltage regulators.

Concerning the corrective methods listed in Mr. Michelson's discussion, we believe the use of voltage regulators to be the most important. The switching of capacitors as dictated by system requirements is next in importance when such requirements arise. The cost and complexity of switching equipment are generally less than is usually believed, so it is suggested that this principle of operation should not be excluded too hastily.

It is presumed that B. M. Jones' first question refers to the effect single-pole switching might have on the stability comparison of the capacitor and condenser. The use of single-pole switching infers single-phase faults, so that longer critical switching times obtain, since the "fault on" shock is not so great. Hence, the stability problem is minimized, so the difference in stability between the two types of kilovar supply—whatever it may be—becomes less important than that indicated by three-phase faults where reclosing is not used as it was in the case studied. Since the difference was small in every case studied, we see no change in the conclusions for single-pole switching.

A concentrated system with no long transmission lines, in general, should have relatively long critical switching times. Without further study it is believed that the stability of the Duquesne system should be better than the system studied.

Mr. Holm seems to feel the case of the synchronous condenser was not presented with due emphasis. The application of condensers in the systems studied was primarily for kilovar supply. The rating of the condenser being determined by the emergency kilovar requirements and *not* by stability. Therefore, the ratings selected were *not* necessarily"—on a rather low side" as Mr. Holm feels. In conclusion 3 of the paper, it is stated that "—neither synchronous condensers nor capacitors having a rating corresponding to the emergency kilovar requirement have much effect on stability—." And continuing, it is stated that appreciable improvement *can be* obtained by using *very large* condensers, so it is difficult to understand why Mr. Holm thinks this "—is an altogether too generalized statement." The voltage chosen for the length of line studied may be on the low side, but this magnifies the stability problem making the differences more important. If no stability problem existed, there would be no reason to be interested in stability, not to mention the small differences obtaining between capacitors and condensers.

As a matter of fact, it is not considered that Mr. Holm's condemnation of data given in Figure 11 is justified on the basis that the example of a 150-mile 132-kv interconnection selected for Table VII (in connection with Figure 11) is poor, because he considers the voltage selected for this line length to be low. The data given in Figure 11 is perfectly general and, as pointed out in the paper, may be applied to *any* line length and *any* voltage, with an appropriate change in kilovolt-ampere base as given by the relation

$$\text{kva base} = \frac{1{,}722\ (\text{line kv})^2}{\text{line length, miles}}$$

Thus, if an extreme example in the direction desired by Mr. Holm is taken and a 287-kv 50-mile line is considered, the kilovolt-ampere base to which Figure 11 would apply is 2,840,000; the intermediate condenser whose rating would be entirely used at the power limit would be 0.27 times this base or 765,000 kva; the power limit would be about 1.02 times this base or 2,900,000 kw; and, if the condenser size were raised to 8,500,000 kva, the power limit would be raised to about 1.19 times the base or 3,380,000 kva—a tremendous number but still only 40 per cent of the condenser rating required! Figure 11 indicates that, for any voltage or line length, going from no intermediate condenser to one 3.0 per unit in size increases the power limit of the interconnection only from 0.91 to 1.19 per unit, or 31 per cent, a fact that to us indicates undeniably that means other than the intermediate synchronous condenser should be considered when interconnected system stability is the problem. However there is no denying its help is in the proper direction.

Conclusions are drawn for the "interconnected" system on the basis of Figures 16 and 17 of the paper no more extensively than they are for the "noninterconnected" system on the same basis. It is recognized that the step-by-step swing curve is less accurate than the equal-area criterion. But the fact that results by use of the equal-area method with a simplified two-machine approach checked so closely those obtained with swing curves and three machines (a longer process less desirable than the former when "mass data" are desired) led to the conclusion that the simplified approach was usable. It may be said that, although we were considering but small differences between the capacitor and condenser cases, the difference between the two-machine equal-area method and the actual three-machine swing-curve method was still smaller.

The combining of the distant system E by closing the switch S (Figure 1 of the paper) was justified by the assumption that the stability problem was between the hydroelectric station and the rest of the system. This is discussed in detail in the second paragraph of the section entitled "Effect on Stability Within One System of Interconnecting With Another System."

Apparently the term "critical switching time" has been misinterpreted. Critical switching time is the maximum time the fault can hang on, and the system still maintain stability. Therefore, the statement that the longer critical switching times favor the capacitor does not say that the longer the fault hangs on the more service the capacitor can render. It does say, however, that, if the fault can hang on longer and stability still be maintained, the capacitor shows up relatively better than if the fault must be removed quickly to maintain stability. It was this fact that lead to the opinion that the results of three-phase faults on a relatively unstable low-voltage long-line system for making the comparison would be conservative from the capacitor standpoint.

It is felt that data already presented[1,2] before the Institute cover the question of relative effects of series capacitors and intermediate synchronous condensers raised by Mr. Holm.

References

1. Closure by J. W. Butler, J. E. Paul, and T. W. Schroeder of Steady-State and Transient-Stability Analysis of Series Capacitors in Long Transmission Lines. AIEE Transactions, volume 62, 1943, pages 379–80.

2. Stability Limitations of Long-Distance A-C Power-Transmission Systems, Edith Clarke, S. B Crary. AIEE Transactions, volume 60, 1941, pages 1051–9.

Aircraft Storage-Battery Design

Discussion of paper 44-112 by John L. Rupp, presented at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944, and published in AIEE TRANSACTIONS, 1944, November section, pages 773–7.

M. F. Gunderson (nonmember; Los Angeles, Calif.): The author states that it should be "definitely determined whether radio shielding is necessary." In view of the incorporation of open-wire circuits in aircraft, it does not appear that battery shielding needs to be considered in future battery design—at least not in *all* types. Installations in many aircraft of recent design do not use the shielded junction box on the battery. However, present battery *AN* specifications call for battery shielding.

Relative to simplifying the servicing and maintenance of the battery and thus increasing the efficiency and life of the battery, the two design changes mentioned which seem to be of greatest importance are:

1. The transparent polystyrene-type molded case makes it easy to check the level of electrolyte in the battery. Mechanics are tempted to neglect servicing the batteries when it involves removing the cover, the plugs, and so forth, especially if the battery is located in an awkward position. A polystyrene-type battery case or some type of visual electrolyte level indicator would reduce the chances of the addition of needed battery water being neglected.
2. Push-on terminals of the type which automatically make connection when the battery is installed would do much to eliminate disconnect troubles, such as loose terminal nuts and short-circuits, and the difficulties encountered because of space limitations.

AN specifications are needed which will cover unshielded bafteries and plug-in-type batteries.

Battery Booster Coils for Airplane-Engine Ignition Systems

Discussion and author's closure of paper 44-121 by A. V. Alvino, presented at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944, and published in AIEE TRANSACTIONS, 1944, September section, pages 672–3.

W. E. Berkey (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Magneto ignition systems on aircraft engines require extra ignition starting equipment, which is in use only a few seconds during starting, to be carried on the airplane. It appears that engine-starting ignition boosters will be necessary, until improved engine cranking can turn over the engine at speeds sufficient to start magneto firing of the spark plugs, or until new ignition systems can be developed to fire the cylinder charge at all speeds.

Figure 1. Magneto secondary voltage to ground with booster operation

Magneto rotation—192-rpm crankshaft, 57.5 sparks per second, 24-volt battery vibrating - contact booster as in Figure 3 of the paper

As pointed out by the author, calculations of ignition systems are practically impossible. However, ignition systems may be studied with the cathode-ray oscillograph. An oscillogram of the magneto output voltage to ground while excited with a booster circuit similar to that shown in Figure 3 of the paper is given in Figure 1 of this discussion. The magneto tested was an 18-cylinder double-circuit type rotating at 192 rpm equivalent crankshaft speed and delivering 57.5 pulses per second. Magneto and distributor gears were driven with an electric motor. Spark gaps firing in air simulated the plugs. The booster vibrator superimposes a series of firing-voltage pulses of one polarity upon the magneto output voltage. The oscillogram shows a higher frequency of pulses from the battery-vibrator circuit, when the secondary magneto voltage is of opposite polarity to the voltage produced by the booster. When the magneto breaker contacts close, the magneto primary and booster-coil current are short-circuited to ground. The secondary voltage is not zero during this period because of a series capacitor built into the magneto coil secondary winding which effectively isolates the magneto-to-distributor high-voltage lead. A 60-cycle timing and calibration wave is shown in the oscillogram in addition to a zero-voltage line.

In the paper three booster ignition circuits are shown, which require batteries as the energy source for ignition starting. The question may be asked, why not use a battery-ignition coil system similar to those used in automobiles? Booster circuits then would be unnecessary. The points usually given in favor of magneto ignition systems for aircraft engines are

1. Greator safety.
2. Increased reliability.
3. Higher firing speeds.

Most airplanes now carry batteries for operating auxiliary equipment so that safety factor does not seem to be an argument. The use of a separate ignition battery with the regular airplane battery as emergency standby would increase the reliability of the battery ignition system. An experimental battery ignition system has been operated at speeds greater than 405 sparks per second, which is sufficient for most modern high-power aircraft engines. Modern developments have resulted in lighter weight batteries, better circuit protective methods, and improved insulation. It appears that a comparison of battery with magneto ignition systems for aircraft engines again would be timely.

It is rather surprising that repeated starting or long periods of booster operation do not demagnetize the permanent magnet objectionably. Have quantitative tests been made upon this point?

A. V. Alvino: It is true as stated in the discussion that higher cranking speeds would eliminate use of boosters for engine starting, but in order to obtain higher cranking speeds starter motors of much higher capacity than present ones must be used. Such being the case, increased starter weight does not warrant elimination of the booster system.

Quantitative tests on demagnetization of magneto permanent magnets have been made by Bendix Scintilla magneto division of the Bendix Aviation Corporation, Sidney, N. Y. I am certain they would gladly furnish any information desired on this subject.

Rating of Electric Equipment Under Intermittent Load

Discussion and author's closure of paper 44-101 by Myron Zucker, presented at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944, and published in AIEE TRANSACTIONS, 1944, November section, pages 778–83.

G. H. Fett (University of Illinois, Urbana, Ill.): It is agreed that the term, "duty cycle," while simple to understand and easy to use, is inadequate to rate properly intermittent loads of the type encountered in resistance welding. The duty factor as defined by the author is a big step forward in getting at the factors to be used in rating equipment.

An extension of the method suggested by the author is developed in this discussion with the hope that it may lead to further work in rating intermittent loads. It is not expected that this extension is immediately practicable, because some of the data required are not available.

The rating of equipment is an attempt to set up a criterion which will give an answer to the question of how long the equipment will operate before the insulation deteriorates to the point where the apparatus fails. The term, "duty factor," satisfactorily specifies the maximum operating temperature in terms of continuous-duty temperature. It does not take into account the fact that insulation destruction depends, not only upon the maximum value of the temperature, but also upon the length of time that the insulation is subjected to each temperature.

Hence it would seem desirable to include another factor in the rating of the equipment. Suppose that the curve of life of the insulation used in the welding machine is plotted as a function of temperature, as

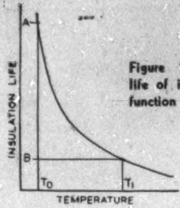

Figure 1. Expected life of insulation as a function of temperature

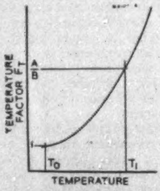

Figure 2. Temperature factor as a function of temperature using T_0 as reference

shown in Figure 1 of this discussion. This curve is known for some electric equipment and could be obtained experimentally. From this curve a temperature factor is to be derived.

For a temperature T_1 the expected life is A units of time. At a temperature T_0 the life is B units. The temperature factor for T_1 is then A/B.

A curve, such as that of Figure 2, is drawn in which the temperature factor is plotted as a function of temperature. This might be thought of as a weighting curve so that the higher temperatures at which accelerated deterioration takes place are given greater weight than the lower temperatures.

Now suppose that the actual duty cycle and temperature variation for the welder are as given in the author's Figure 12. Each ordinate of temperature is multiplied by the corresponding temperature factor F_t and a new curve of this product as a function of time is drawn, as shown in Figure 3 of this discussion. The mean value of this curve, indicated by M in the figure, is the temperature to be used in comparing the welding equipment. It is seen that by this method the value of M obtained actually does take into account the variation in destruction of insulation at different temperatures.

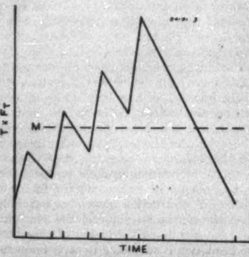

Figure 3. Temperature factor times temperature as a function of time during operating cycle

M is the mean value averaged over a complete cycle

Myron Zucker: Mr. Fett's extension of the heating analysis is certainly in the right direction. It would be real progress if we could get the necessary information on rate of deterioration, express it analytically, and perform the integration indicated in Mr. Fett's Figure 3. Then we would have a good measure of severity of service due to a given duty cycle. What we actually want is not an average temperature, but an expression for the integrated deterioration of the equipment subject to periodic load.

The Design of Low-Voltage Welding Power Distribution

Discussion and authors' closure of paper 44-105 by Comfort A. Adams, John R. Fetcher, and Arthur C. Johnson, presented at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944, and published in AIEE TRANSACTIONS, 1944, pages 1180–90.

G. H. Fett (University of Illinois, Urbana, Ill.): The authors have contributed a complete and unique solution to the problem of the design of the feeder system for resistance-welding equipment. The method takes into account the fact that there is a probability of a number of welding machines on the same feeder being in operation at the same time. One basis for the design of the feeder is that the probability that the number in operation at any one time in excess of the number for which the feeder is designed is equal to the probable number of poor welds which are permissible. This leads to what may be thought of as the most economical design of the feeder system, since the overdesign to accommodate all the machines simultaneously is avoided. In addition the authors have shown how to check the design for thermal and mechanical effects.

One point which the authors did not mention specifically is the possibility of mechanical resonance and consequent vibration of the feeder. The feeder consists of two flat parallel bus bars of copper separated by a thin insulating slab and clamped together with insulating covers. Because of the currents in the feeder there is a periodic force tending to push the bars apart, the periodicity of the force depending upon several factors.

First, there is the periodicity caused by the frequency of alternations of the current, since, each time the current passes through zero, the force between the conductors is also zero. This gives rise to a pulsating force at a frequency twice that of the system.

Second, the periodicity of the welding operation will give rise to an average force which changes with the amount of on- and off-time. In the case of a feeder supplying a number of machines the probability is small that a periodic force will exist over a time long enough to cause serious mechanical vibration of the feeder. In any event, the fundamental frequency caused by this effect will be smaller than the system frequency.

If one bus bar of the feeder is thought of as a uniformly loaded beam (with the load in the horizontal direction), having fixed ends provided by the clamps, the fundamental frequency of oscillation is given by

$$f_n = 7.13(EMg/wl_e^4)^{1/2}$$

(assuming no damping is provided by the insulating covers), where E, M, and l_e have the same meaning as in the paper, g is the gravitational constant, and w is the weight per unit length of the bus bar.[1] If the design constants specified by the authors, namely, $l_e = 16.5$ inches, $h = 4.25$ inches, and $t = {}^3/_{16}$ inch are used, then the value of the fundamental frequency of oscillation becomes $f_n = 196$ cycles per second.

The next natural frequency is 2.75 times the fundamental, or 540 cycles per second. The third is 5.4 times the fundamental or 1,060 cycles per second.

It is clear from these calculations that the likelihood of the bus bars' vibrating in resonance with the applied force caused by the pulsations of current due either to the system frequency or to the welding frequency is small. It is to be noted that the damping effect of the insulating cover caused by the friction between the bus bar and the cover will tend to reduce both the natural frequency and the amplitude of the oscillation. If the damping is so great that the natural frequency is reduced from 196 to 120 cycles, the effective Q of the mechanical system is reduced to a value of only 0.61.* This means that the increase in deflection of the bus bar, if resonance occurred at 120 cycles because of the damping, is small indeed.[2]

The critical spacing of the clamps for the bus-bar dimensions given by the authors for resonance at 120 cycles can be obtained from the foregoing equation and is

$$l_e' = l_e(196/120)^{1/2} = 21 \text{ inches}$$

again assuming no damping.

Have the authors observed any tendency toward vibration of bus bars for other dimensions or spacings?

References

1. Mechanics Applied to Vibration and Balancing (book), D. L. Thornton. John Wiley and Sons, Inc., New York, N. Y., 1940. Page 305.

2. Introduction to Circuit Analysis (book), A. R. Knight, G. H. Fett. Harper and Brothers. New York, N. Y., 1943. Page 249.

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*If f_0 is the frequency of the applied force and is to be the same as the frequency of the mechanical system with damping, then, if an electrical analogy of a series circuit is used as a first approximation, $2\pi f_n = (1/LC)^{1/2}$ and $2\pi f_0 = (1/LC - R^2/2L^2)^{1/2}$ at amplitude resonance. (See reference 2, page 254.) Let $2\pi f_0 L/R$ represent the Q of the circuit. Then $1 = [(2\pi f_n/2\pi f_0)^2 - {}^1/_2Q^2]^{1/2}$. Solution of the expression for Q gives $Q = [{}^1/_2/(f_n/f_0)^2 - 1]^{1/2}$. The larger the value of Q the greater will be the amplitude of the resonant peaking of the charge in the electric circuit and, by analogy, the deflection of the mechanical system. A value of Q less than one means virtually no peaking at all since the friction is so great.

L. W. Clark (The Detroit Edison Company, Detroit, Mich.): The new low-reactance bus design described by C. A. Adams, J. R. Fetcher, and A. C. Johnson should prove a welcome addition to the several designs now commercially available for use in resistance-welder installations. There appears to be no question but that this arrangement of copper bars with a high width-to-thickness ratio placed in close proximity produces a bus with a minimum of reactance at a reasonable cost. A similar bus that has been commercially available for some two or three years consists of 1/4- by-4-inch coppers with 1/4-inch separation between the inner faces of the bars, instead of the 1/16-inch separation which characterizes the bus described in the paper. For the same copper cross section the reactance is reduced in a ratio of about 2 to 1 with the separation reduced from 1/4 inch to 1/16 inch.

The authors do not go into detail as to how the circuits are tapped off from the bus. It is conceivable that with only 1/16-inch space between bars there might be some difficulty in designing suitable tap connections for the individual welder feeders, and it would be interesting to know how this was done.

The authors have based their selection of a five per cent voltage drop for the secondary bus itself on the assumption of a ten per cent total allowable voltage drop including step-down transformers and the high-voltage supply ahead of the transformers. This should be satisfactory for conditions in large industrial areas where ample power is available. It should be cautioned, however, that there are many locations throughout the country where it would be difficult as well as uneconomical to keep the combined voltage drop in the high-voltage system supplying the plant and in the step-down transformers within five per cent. Every installation, therefore, should be studied individually and the voltage drop in the respective parts of the system designed to meet the local conditions most economically. In some cases it will be found cheaper to design the secondary bus for some extremely low voltage drop, say one per cent or two per cent, to allow for a larger voltage drop in the balance of the system without exceeding a total drop of ten per cent.

The example of 15 machines cited in the paper, each making a 10-cycle weld every two seconds, represents an average loading of the supply circuit considerably higher than ordinarily encountered. No mention is made as to the type of welding machines used, but one would suspect from the rapidity with which the welds are made that they must be portable gun welders. It is not often that a production rate as high as one weld every two seconds is maintained on manually operated floor-mounted machines.

Many satisfactory installations have been laid out on the basis that the total voltage drop shall not exceed ten per cent with only the largest welder of the group in operation. Of this total drop perhaps 1/3 or 1/2 is in the secondary-supply bus or circuit. In these cases there have not been more than five or ten relatively large machines on the circuit, and the rate of production has been such that probably not more than two of the large machines "hit" together oftener than about once in every 5,000 or 10,000 welds produced by the group. With the number of machines cited in the paper and the high production rate given, it would become necessary to design the bus in such a way that it would be possible for more of the machines to weld at the same time without causing excessive voltage drops.

It is gratifying to know that the authors, through use of the laws of probability, have developed a method for calculating the chances of machines hitting together. It is unfortunate, however, that the formulas and mathematics are so complicated. Most engineers are not familiar with the higher mathematics involved, nor can they take the time for such lengthy calculations. It is hoped, therefore, that some short cuts and simplifications can be devised, the use of which will result in acceptably accurate answers.

As an example of one such simplification, it has been suggested that in designing a bus to serve a group of welders, the equivalent continuous current in the bus be taken as the equivalent continuous current of the largest machine plus 60 per cent of the sum of the equivalent continuous currents of the remaining machines on the bus. It is relatively simple to calculate the equivalent continuous current of each machine by multiplying the maximum current during the weld by the square root of the duty cycle.

An opportunity to test the accuracy of this method was presented in the case of a plant having five welding circuits, all supplied from a common feeder. The types of machines included spot, projection, flash, and seam welders in a wide range of sizes. After the plant was in production, tests were made to determine the equivalent continuous current in each circuit and also the maximum current and operating duty cycle of each machine.

The following table shows the comparison of the calculated probable equivalent continuous current (column 3), the actual equivalent continuous current as measured by thermal-type ammeters (column 4), and the maximum possible equivalent continuous current, if all machines hit together (column 5). The test indicated that the relatively simple method proposed for calculating the equivalent continuous current was reasonably accurate and in all cases conservative. The authors' calculated data for their example of 15 machines also is included in the table. It would be interesting to know whether the authors have any supporting test data on installations described in their paper.

Table I

1 Circuit	2 Number of Machines Per Circuit	3 Calculated Probable Equivalent Continuous Current Amperes	 Per Cent of Column 5	4 Actual Equivalent Continuous Current by Test With Thermal Ammeter Amperes	 Per Cent of Column 5	5 Maximum Possible Equivalent Continuous Current, Amperes
A	4	320	78	305	75	410
B	6	340	72	255	54	470
C	6	705	75	380	40	935
D	7	515	75	520	75	690
E	13	630	72	610	70	875
*F**	36	2,174	64	1,320	39	3,380
AIEE paper (44-105)	15	674	38	..		1,780

*Common feeder supplying circuits *A*, *B*, *C*, *D*, and *E*.

D. L. Beeman (General Electric Company, Schenectady, N. Y.): As pointed out in this paper, careful design of the low-voltage circuits is extremely important in producing a distribution system with satisfactory voltage regulation for the operation of groups of welders. The impedance of primary lines and substation transformers also may be an appreciable factor in contributing to the total voltage drop. The apparent reactance of these parts of the circuit may be reduced to substantially zero by the use of series capacitors. Series capacitors unlike voltage regulators substantially are instantaneous in their operation.

The shorter these secondary runs the less the voltage drop, hence, it is desirable to install the transformers at load centers and to use several smaller substations with short secondary runs instead of one large substation with long secondary runs. If there is a wide diversity of the load, the smaller transformers may be interconnected on the secondary side to form a secondary network so that the load is divided among several transformers.

The application of the principles of secondary-circuit design outlined in this paper plus the adoption of smaller transformers near the load centers and series capacitors to neutralize primary-circuit and transformer impedance, should enable the design of power-supply systems for welders in almost any concentration without objectionable voltage drop.

C. M. Rhoades, Jr. (General Electric Company, Schenectady, N. Y.): This paper presents the fundamental approach to the problem of determining the loading and regulation of a single low-voltage welding feeder when the number, magnitude, and frequency of application of the loads are known.

It readily can be seen that before making such an analysis, production, welding, and plant power engineers must contribute accurate data, if practical benefits are to be obtained. The material in this paper should aid the plant power engineer materially in contributing his share of the information needed in planning new or revised welding facilities.

The illustration of the bus-type feeder

described in the paper indicated a long single-phase feeder. The question arises as to whether or not changes of section are made along the length of the feeder or whether the same size copper is used throughout. Welders are apparently distributed uniformly along this feeder and, thus, the load decreases with increased distance from the distribution transformer. It is interesting to note that, if separate feeders consisting of two conductor concentric cables are run to each welder, the cables can be loaded to their full thermal limit on duty-cycle basis without exceeding five per cent voltage drop when the length of the feeder is approximately 300 feet or less. This suggests the possibility of centrally located unit substations with radial feeders to surrounding welders. In this way interference between welders on a feeder is eliminated, and the problem becomes one of determining the effect of several welders on the regulation of the distribution transformers. It would be helpful in completing the over-all picture of welder power supply, if the authors would comment on the selection of substation size and the economies of radial feeds versus bus-type distribution. Where the load density is high enough, it would appear that over-all voltage variation could be minimized by feeding centrally located unit substations at high voltage and making secondary runs as short as possible.

Comfort A. Adams, John R. Fetcher, and **Arthur C. Johnson:** The authors wish to acknowledge the comments of L. W. Clark, G. H. Fett, C. M. Rhoades, and D. L. Beeman, and to thank these men and the others who participated in the oral discussion of the paper at St. Louis, for their interest.

The authors do not propose the low-reactance bus feeder of the type discussed in the paper as a cure-all for the problems encountered in resistance-welder power supply. The bus-type feeder is rather an addition to the already long list of devices and methods available to power supply and welding engineers as aids to solving their problems.

The analyses we have presented are merely guides to be applied to the design of welding power distribution systems. They are based on a number of assumptions, some of which limit the scope and accuracy of the calculations but which are nevertheless necessary, if anything which even approaches a rational analytical solution is to be obtained.

Following are the answers to the points raised by Mr. Clark.

The feeder is built in 20-foot prefabricated sections. The copper is about two inches longer than the insulation at each end and bent at right angles to it. The prefabricated sections are installed about eight inches apart between ends. Where there are no load taps, the ends of the sections are joined by semiflexible jumpers bolted to the right-angle flanges, thus allowing for expansion and misalignment. Where there are load taps, the jumpers are replaced by copper bars of the same size, in the shape of a channel and bolted to the ends of the feeder sections. An L-shaped section to which the riser can be bolted is brazed onto the channel-shaped jumper.

The ten per cent over-all voltage-drop variation is a sound criterion and should not be exceeded, if consistent welds are expected. Since the criterion specifies a variation in voltage drop, a considerably greater drop can be allowed from a weld-quality standpoint, if only one machine is permitted to operate at a time. Mr. Clark's caution that in certain cases it may be necessary to allow for a larger drop in the high-voltage system and transformers is sound.

The application of the probability formulas is perhaps not as formidable as it may at first appear. The notation used merely indicates the sum of a series of terms, each of which is readily evaluated. The theory on which the formulas are based is perhaps complicated, but the application is a purely mechanical process, which can be mastered in a few minutes.

The accuracy of the results obtained obviously will depend upon the soundness of the approximations as to equivalent average load and duty cycle and will vary for each installation. Some such estimate must be made in any case, but, once made, the determination is rational from there on. Since this estimate is vital to the soundness of the results, it may be well to point out that it is not sufficient merely to average the duty cycles but that weighted averages are necessary. This is where the discretion of the engineer comes in. In fact, there are many special cases where this estimate involves special methods and sound engineering judgment.

Any of the more empirical methods of determining the rms feeder current, for example that proposed by Mr. Clark, is bound to yield results with widely varying accuracies as shown by his own data. Taking an extreme case where there may be as many as 20 welders on the same feeder, the total feeder current would be substantially two thirds of the sum of the rms currents of the 20 welders. This would take some account of the fact, pointed out by the authors, that the rms feeder current is less than the sum of the individual rms currents. Thus the difference between the two methods is that between taking this assumed or empirical percentage or the calculated percentage based upon sound probability theory and upon a careful estimate of the equivalent current and duty cycle per machine. In other words, the authors have tried to eliminate a part of the empirical process.

To answer Mr. Fett's queries, the authors have encountered no trouble from vibration in any of the bus-type feeders which have been installed or were built for test purposes. The $^3/_{16}$-by-4-inch feeder has carried overload currents as high as 4,000 amperes with no more indication of vibration than a faintly perceptible hum. Mr. Fett's formulas probably are correct, but he has overlooked a detail that renders them inapplicable to this particular case. The bars are free to deflect only in one direction, and, hence, almost perfectly damped. There is consequently no natural frequency of vibration.

In reply to Mr. Rhoades, the feeders the authors have used are of uniform cross section throughout their length. The plant layout is such that the feeder lengths from any one substation are not fixed. Separation between substations is effected by omitting jumpers between lengths of bus. Hence the substation load can be adjusted by changing the feeder length. If necessary, one or more of the substations can be completely isolated and by-passed by the feeder. The welders are not necessarily distributed uniformly along the feeder length.

The authors realize that a discussion of the economics of plant layout and substation size are pertinent, but a complete discussion of such problems would make fine material for a book on the subject. Given a rational basis for the design of the equipment, engineers are capable of comparing different arrangements to determine which best suits their needs. Thus the economics of the problem were discussed only insofar as it was necessary to show that the authors' proposals could compete with existing practices.

Whether or not separate feeders to each welder can be used would depend on the plant layout, the load density distribution, the current required by each machine, the individual duty cycles, the thermal time constants of the feeders in question, and the standard sizes of concentric cables which are available. An offhand judgment without much investigation would seem to indicate that separate concentric feeders to each machine would be considerably more expensive. The heaviest 440-volt feeder the authors have designed is less than 1,000,000 circular mils in cross section and will handle as many as 18 machines ranging in size from 70 to 500 kva, operating on a relatively low duty cycle.

The answer to Mr. Beeman's question is that the use of capacitors is obviously an asset from the operating standpoint. The deciding factor is usually the over-all economy, although in the minds of some executives the additional first cost looms unduly large.

The question as to the optimum number of substations is one to be decided for each particular case and on the basis of sound engineering judgment. It should be noted, however, that with low-reactance feeders satisfactory regulation may be obtained for lengths up to several hundred feet without excessive cost and that the load factor on such long feeders is correspondingly higher for the same load density.

An Improved Electronic Control for Capacitor-Discharge Resistance Welding

Discussion and authors' closure of paper 44-125 by H. J. Bichsel and E. T. Hughes, presented at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944, and published in AIEE TRANSACTIONS, 1944, pages 1150–7.

E. M. Callender (Edward G. Budd Manufacturing Company, Philadelphia, Pa.): One of the major problems today in capacitor-storage-discharge welder design is the attainment of high-speed repetitive welding with more stored energy for some types of work than is now available. Typical jobs include high-production single-spot welding and roller spot welding such as would be used in the manufacture of seamless tubing.

It commonly is stressed that the advantage to capacitor-discharge welders is their low demand on the power source. This is true as long as there is no limit on charging time. However, when the charging time is reduced greatly and the capacity is high, the power demand becomes as great as with most a-c resistance welders. When the roller spot-welding speeds are attempted, it is not a simple matter to charge more than a small bank of capacitors without undue tax on rectifying equipment and line regulation. The paper brings out the disadvantage of released electrode sparking at high rates of operation for single spots, which is a detriment.

The authors have worked with this problem of high operating speeds in a novel manner. Their arrangement provides:

a. Reverse current flow that leaves capacitors partially charged following weld.

b. Flux reset method that reduces the residual energy of welding transformer considerably, reducing electrode sparking.

c. Forging pressure application, which is highly useful.

Undoubtedly the flux-reset method would decrease sparking on single-spot work. Whether it provides reduction in over-all work positioning time compared to the conventional short-circuit switch method depends on the actual apparatus performance. The problem of sparking does not exist in the design of a closed-roll discharge welder providing high speeds and output.

Some disadvantages to the authors method employed in the paper come to mind.

1. Large proportions of reverse current necessitates derating the capacitor-bank storage voltage.

2. The reverse-welding-current loop is much smaller and will not contribute to weld fusion, acting only as an annealing agent. Weldability is also less than for an equal shunt tube system.

3. Common objection to usual discharge welders, perhaps necessarily so, is that they are very cumbersome and involve much apparatus.

Comment 2 should be given further explanation. It is believed that the magnitude of the second current loop is too low to permit further weld fusion. We have found that a fairly large percentage of the peak current must occur before fusion takes place. If the current once falls below this level, as when it drops through zero, heat leakage precludes further weld fusion unless considerable energy again is imparted. Heat leakage is most pronounced with aluminum but may also be a factor with stainless steel.

Because some energy is returned to the capacitors, the weldability will be somewhat less for the author's machine, than a regular shunt tube discharge unit having the same conditions imposed.

The author's have taken unique steps to improve machine speed, and perhaps of most interest is their use of the flux reset. It should be remembered that the stored-energy welder is one of the newest devices in the field of resistance welding, and many strides have and will be made in its further development.

H. J. Bichsel and B. T. Hughes: Mr. Callender in his discussion has summarized in brief form a number of factors influencing the design of the capacitor-discharge control.

He has added further emphasis to the need for increased speed of operation which was also one aim of the design engineers in the development of the improved control.

There are certain general considerations that must be understood clearly before attempting to compare relative kilovolt-ampere demands for the capacitor-discharge system and single-phase a-c resistance-welding systems to weld an equivalent thickness of material.

The capacitor-discharge system was developed to take advantage of all the time available between welds to store electrical energy to produce the desired weld current. The rate at which energy can be delivered to the capacitor bank and the corresponding rate at which energy can be taken from the supply system (kilovolt-ampere demand) therefore largely depends on the time that is permitted between welds, other factors being equal.

In the welding of thin material at 200 spots per minute, for example, 0.020-inch-thick aluminum, the kilovolt-ampere demand from the three-phase control described in the paper would approach the kilovolt-ampere demand for a single-phase welder for the same thickness material. The fact that the demand is divided among three phases of the supply system does provide an advantage.

In the welding of thicker materials, for example 0.064-inch aluminum or thicker, the weight of the structure to be welded, the additional heating of the electrodes, the somewhat slower operating speed of the machine, and the important factor of more frequent electrode cleaning all combine to make speeds considerably less than 200 per minute desirable. The corresponding ratio of time of weld current flow to total operating time becomes very small which means that real reductions in the kilovolt-ampere demand can be obtained.

Where higher speeds are required from the control with high-capacity setting, it is not difficult to alter the design of the rectifier and main power transformer to obtain the short-circuit direct current and charging rate desired. This has been accomplished successfully in the design of the capacitor-discharge control for roll spot welding machines. It is also important to note that balancing of the power demand among the three-phase supply has even greater significance where the maximum demand is about 100 kilovolt-amperes or greater.

It is also important to understand clearly that standard spot-welding machines and roll spot-welding machines represent two distinctly different types of equipment with separate fields of application. The roll-type welder has a tremendous speed advantage as long as flat stock is to be welded. However, this equipment cannot be useful when welds must be made in complicated structures with curved surfaces in three dimensions or for the welding of flat material where the spots are not in a straight line.

During the preparation of this paper it was found that adequate presentation of both the electrical and metallurgical characteristics of the full-cycle capacitor-discharge system could not be made. It was therefore necessary to eliminate any metallurgical discussion. However, during the development of this control, its welding characteristics were investigated experimentally for all thicknesses of aluminum where tests showed the full-cycle current could be obtained. It has been found that the full-cycle system has no detrimental effects on the weldability of aluminum. Aluminum of the same thickness can be welded with the same microfarad capacity, charge voltage, and transformer turn ratio for either a full-cycle or unidirectional discharge. Further analysis of the welding action with the capacitor-discharge system has confirmed the experimental data. When aluminum is being welded, the narrow plastic range of the material near its melting temperature plus the high thermal conductivity prohibits keeping the metal between the welding electrodes at melting temperature for an appreciable period of time. The fused metal would be rapidly blown out, if too much heat is applied. Therefore, fusion of the metal is accomplished with only that portion of the discharge current wave near the initial peak value. Solidification of the material is complete before the current wave has decreased appreciably from peak value. The wave shape of the remainder of the discharge has little effect on weld strength or appearance, so long as any additional peaks in the discharge current remain appreciably less than the initial value.

Two important advantages of the full-cycle discharge as pointed out in the paper are:

1. The unused energy dissipated in the shunt tube can be returned to the capacitor banks as usable energy to aid in the next weld with the full-cycle discharge.

2. The problem of electrode sparking is eliminated by the second half cycle of reverse-discharge current. The residual magnetic energy in the welding transformer is reduced to a very small amount at the end of the discharge by this reverse current flow. It is interesting to note that, when operating at 480-microfarad settings or lower for the capacitor bank, the inverse current in most cases is sufficient to reset the flux without the aid of the flux-resetting rectifier. The flux-reset rectifier is of little value in eliminating electrode sparking, as it can exert little effect on the welding transformer before the electrodes have been released. This is contrary to the comments regarding flux reset brought out in the discussion.

The reverse-current loop does influence the maximum voltage to which the capacitors may be charged. However, the maximum permissible charge voltage is still greater than the values commonly used by most operators of this equipment and the detrimental effects of derating the capacitors are of minor consequence. Present ratings on the standard 2,500-volt 120-microfarad capacitor are:

1. For operation in combination up to 1,200 microfarads the maximum charge voltage is 2,500 volts.

2. For operation in combination from 1,200 to 2,640 microfarads and higher the maximum charge voltage is 2,800 volts.

These ratings are designed to work on most existing designs of capacitor discharge-machines.

In the mechanical design of the capacitor-discharge system it has become standard practice to combine all electrical control in a single cabinet. It is true that this involves considerably more control equipment than is required for an a-c single-phase welder.

However, in the case where the kilovolt-ampere demand on the single-phase power supply is sufficient to require a series-capacitor bank, it is interesting to note the amount of equipment involved to provide circuit-control functions equivalent to those built into the single capacitor-discharge cabinet. It would be necessary to provide a synchronous welding timer with heat control and associated power ignitrons, a rack of series capacitors, a sequence-timing panel, a current-compensator panel, a d-c forge-delay-timing panel and in many cases a power autotransformer to obtain the correct voltages across the primary of the welding transformer.

If this a-c control equipment were to be mounted in a single cabinet, its size would compare favorably with the capacitor-discharge control.

The authors wish to thank Mr. Callender for his discussion.

American Telegraphy After 100 Years

(44-127)

Developments in the Field of Cable and Radio Telegraph Communications

(44-126)

Telegraphy in the Bell System

(44-128)

Discussion and an author's closure of papers presented at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944, and published as follows in AIEE TRANSACTIONS, 1944:

44-127 by F. E. d'Humy and P. J. Howe, pages 1014–32.

44-126 by Haraden Pratt and John K. Roosevelt, pages 1044–7.

44-128 by J. A. Duncan, R. D. Parker, and R. E. Pierce, pages 1032–44.

F. B. Bramhall (Western Union Telegraph Company, New York, N. Y.): Three valuable papers on record communication in this country were fittingly presented at the St. Louis summer technical meeting, a full session being devoted to the marking of 100 years of practical telegraphy. Each of these papers stands admirably on its own feet as a report on the present state of the art and the practices of these corporations which the respective authors represent. The paper by d'Humy and Howe, however, is not a complete report on land-line telegraphy unless augmented by the excellent paper of Duncan, Parker, and Pierce on the customer-to-customer teletypewriter exchange service rendered by American Telephone and Telegraph Company. So, likewise, the paper by Pratt and Roosevelt gives an incomplete picture of present day technique in ocean-cable telegraphy without that section of the d'Humy–Howe paper which outlines Western Union Company's modern ocean-cable practices.

About a decade and a half ago, the development of cable-repeater and terminating equipment received a strong impetus when Western Union installed its first high-speed loaded cable. Since that time, the romance, the delicate instruments, and the trial and error methods of duplexing have disappeared largely from that company's cable system. Electronic devices, vacuum tube, repeaters, and terminal amplifiers now are used. Many cables now are operated with automatic printing telegraph equipment essentially identical to that used on land lines. The progress, perhaps, may be summed up easiest by pointing out that teleprinter customer-to-customer service between this country and Europe is now provided by the varioplex sytem described briefly in the d'Humy–Howe paper.

Haraden Pratt: The authors thank Mr. Bramhall for rounding out the technical picture concerning cable operating technique. Our original paper was intended to cover only the development of the art as applicable to the cable operating companies of the International System.

Impulse Strength of Insulated-Power-Cable Circuits

Discussion and authors' closure of paper 44-130 by Herman Halperin and G. B. Shanklin, presented at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944, and published in AIEE TRANSACTIONS, 1944, pages 1190–7.

L. I. Komives (The Detroit Edison Company, Detroit, Mich.): This paper, in the opinion of The Detroit Edison Company, is a very valuable and useful addition to the literature on this subject. We believe that the authors have done an excellent job in co-ordinating the data available, and that this paper will be a widely quoted reference. We agree with most of the major conclusions which the authors have drawn and would like to make observations only on some.

The 1,200 volts per mil recommended for use as safe withstand impulse strength based on average insulation thickness is acceptable for average installations of paper-insulated cables. It is too low a figure for installations at higher potentials and under better than average conditions (the two being almost synonymous), such as where large conductors are used and means are taken to assure permanency of impregnation (oil-filled or Beaver-type gas-filled cables, for instance). Extra-high-voltage cables are designed on the basis of impulse strength. The adoption of 1,200 volts per mil in case of a 132-kv cable would mean that the insulation thickness could not be less than 540 mils with 650-kv insulation level. If a manufacturer would design his cable to meet the practice of The Detroit Edison Company and abide by the specifications recommended in this paper, the results shown in Table I would obtain.

The manufacturer therefore easily could meet the requirements in case of the lower-voltage cables which are designed more on mechanical than electrical basis. However, in the really important case of extra-high-voltage cable his hands would be tied.

A comparison of Figure 1 with Figure 2 might have warned the authors that the maximum stresses should be taken into account. If the straight-line average in Figure 2 is accepted as correct, the line drawn through the average values in Figure 1 cannot be a straight line.

The values on oil-filled cables in Figure 1 below the average line all come from one manufacturer who made the tests more than ten years ago when the making of oil-filled cables had not yet been an established art. Why should we penalize the industry because of a few outmoded cable samples? Oil-filled cables of the same manufacturer made later, shown between the 500 to 700 mils level, are all above average. The two results on solid-type cables in the same range, which are below the average line, were obtained in Detroit on cables made before 1931 and the impregnation of which was only fair.

If this paper is to be used as an industrial standard for the design of extra-high-voltage cables, we would like to see a note added on Table I of the paper under oil-filled-type paper from 69 to 230 kv as follows: "For conductor sizes above 400,000 circular mils, insulation thicknesses may be reduced if maximum voltage gradient does not exceed 1,800 volts per mil, with the effect of stranding omitted." The 1,800 volts per mil maximum voltage gradient used in this note is based on calculations of maximum stresses obtaining in commercial oil-filled cables having approximately one-inch conductor diameter, 540 mils insulation (for 132-kv service) and meeting the insulation level requirement of 650 kv.

We disagree with the authors' statement in connection with Figures 3 and 4 that the data available show little or no effect on the impulse strength of the insulation due to service or test aging except for special cases. We feel that in view of the con-

Table I

Rated Voltage, Kv	Desired Impulse Breakdown Voltage, Detroit Edison Co., Kv	Insulation Thickness, Inches		
		Specified 1,200V/Mil	Present Practice	Desired in Future
120	750	0.625	0.600	0.500
24	200	0.167	0.281	0.275
4.8	100	0.084	0.156*	0.125 minimum

* To ground with ungrounded neutral.

sistent showing of lower impulse strength of poorly impregnated cables on Figures 3 and 4 this should be taken into account. We would like to see a statement made that service or test aging resulting in poor impregnation will decrease the impulse strength of the cable.

The question of impulse fatigue not reducing the impulse strength in case of repeated impulses is a very interesting one. What the authors state is generally accepted as being true, however nobody has offered a satisfactory answer as yet.

In Detroit we have observed that while repeated shots do not decrease the impulse strength of cables, they seem to decrease the breakdown voltage of joints and terminals. It may be that the radial strength (straight through) of a cable is much lower than longitudinal (leakage path), while in joints or terminals the opposite may be true.

The shape of the wave appears to be of minor importance when compared to other variables according to the authors' statement. This is in line with our thinking in the matter. In many tests when we did not have the capacity to obtain a full wave of sufficiently high potential to enable us to use the multiple shot method, we quite often used the single shot method with no resistances in the circuit. We trust that results of such tests will be directly comparable with other tests where full waves were obtained.

In connection with the impulse strength of varnished-cambric- and rubber-insulated cables, we feel that there will be no change with aging. Although there is no direct evidence available for this, some statement to this effect may be made.

On Table I in the paper basic impulse insulation levels for equipment are shown. We would like to remind the authors that another AIEE committee is working on standards for potheads. In their proposed tabulation they include voltage classes of 0.6, 92, 196, 287, and 345 kv. Also a 7.5-kv class is included instead of the 8.7 on Table I. This committee proposes two sets of impulse withstand voltage values, one for indoor and one for outdoor potheads. For indoor units the figures on Table I are correct. For outdoor units the corresponding values are: for 1.2 kv-45, 2.5-60, 5.0-75, 8.7-95. It would seem appropriate that these various AIEE committees get together and correlate their findings.

R. W. Atkinson (General Cable Corporation Bayonne, N. J.): This paper well serves the very useful purpose stated for it by its authors. The present discussion is primarily for the purpose of proposing the possibility of using these data for obtaining additional conclusions.

As indicated by the authors, Figures 1 and 2 give little basis for choice between calculation on maximum or average stress. However, analysis of the original data might well show that the high and low points of Figure 2 are explainable on the basis of the specific quality of the insulation in terms of surge strength. Also, the low points on Figure 1 may appertain to tests of cables on which the ratio of maximum to average stress is particularly high. Such data would indicate that maximum stress is, in fact, controlling. (Or the opposite conclusion might be indicated by other data.) If pertinent supplemental data can be secured, it would be very desirable to do this and to include, in the closing discussion, tabulation of these data so that explanation may be available as to why certain tests do not conform to the main pattern. Such supplemental data can be furnished for the tests by the writer's company.

How such further conclusions may be drawn is well illustrated by the data in Figure 5 for gas-pressure cable. In this figure are grouped together the data for high- and low-gas-pressure cable. The conclusion is quite properly drawn from this figure that the average and minimum lines for these tests are in line with those of other paper-insulated cable. However, if the points shown for high-gas-pressure cable are separated from the others it will be observed that these points are well above the average for saturated paper insulation. Further if we select from these points those applicable to the commercial high-gas-pressure cable at Detroit and plot the data on Figure 2, thus taking account of maximum instead of average stress, the very high strength of this type of high-gas-pressure insulation is still more striking. We do not consider that this means that high-gas-pressure insulation is, in itself, higher in surge strength than fully saturated insulation. We do believe the high surge strength of this cable results from its being strand shielded and because the innermost tapes were thinner than usual commercial insulation.

Separating the data for low-gas-pressure insulation from Figure 3 and comparing it with that in Figure 2 shows that the strength for surge voltage of low-gas-pressure cable, while completely adequate for its purpose, is distinctly lower than that of fully saturated insulation.

The authors recommend standardizing surge tests on the basis of the AIEE $1^{1}/_{2}$ x 40-microsecond wave. There is a sound basis for this in view of current practice and because most apparatus available for surge testing is adapted for such a wave. Moreover there is good basis for belief that results of such tests will not be much different than for switching surges. It seems, important, however, to have a great many more direct measurements under a wave comparable to switching surges. We have made a number of such tests and are setting up equipment to do this on a larger scale.

Victor Siegfried (Worcester Polytechnic Institute, Worcester, Mass.): The authors have presented interesting and worthwhile data on a proposed standard withstand test. While such a test may be in the direction of progress, it should be borne in mind that there should be some criterion established which does not require destructive tests for routine checks. The preparation time and detail in analysis required in the proposed tests are tremendous, running up to 10 to 20 hours for one sample, according to my experience. While this may be necessary for definitive tests, the usefulness of impulse testing will depend upon finding ways of interpreting data similar to the power factor tests of the sine-wave testing.

Of interest in this direction is the curve of Figure 7 of the paper, in which breakdown time has a stepped inverse-time relation to voltage. This has been noted for other solid insulations and suggests an approach to the fundamentals of breakdown performance, through investigation of the transition effects at the different steps. Impulse currents fed to these samples in different wave fronts may be the clue. Comparison of the optical and dielectric properties of some of the newer materials which exhibit these stepped characteristics in both realms may yield another clue in this broad problem. Fundamental concepts of insulation breakdown need further investigation such as is covered by this paper, and should have further co-ordination with breakdown studies of other groups interested in insulation. It is my hope that there will be opportunities for continued researches along lines leading to more basic feelings for the mechanism of breakdown, and consequently to more efficient use of insulating materials.

P. L. Bellaschi (Westinghouse Electric and Manufacturing Company, Sharon, Pa.): Experience with transformer insulation has long since indicated to me that, in general, cables would meet present impulse levels for power apparatus. At various times we have also tested short sections of cables.

To illustrate, paper insulation between conductors in oil-immersed transformers may well attain an impulse strength of 1,000 volts per mil. Varnished-cambric insulation, we have found, is lower. Rubber comes further down the ladder. For instance, impulse tests we conducted six years ago on rubber-insulated cables show similar results as in the lower portions of Figure 9. To illustrate again, a 400-mil thickness of rubber-insulated cable would break down at about 170 kv.

Oil-impregnated paper in transformers and in other apparatus has an impulse strength 2 to 2.5 times the 60-cycle crest value of the one-minute-hold test. In view of the data on the volt-time characteristic of oil and of oil-impregnated insulation in reference 17, and from additional tests since, I am inclined to question Figure 7 of the paper.

A volt-time curve such as Figure 7 should be anchored at several points; for instance, at $^{1}/_{4}$ microsecond, at 1 microsecond, and beyond including 500 or 1,000 microseconds, then, possibly, at 0.1 second and beyond including one minute. In each case the number of applications and the method of test should be stated. It would be of practical interest to have such a curve for rubber, as we now have for other insulation materials.

In view of the impulse strength of cables these should render a good account of themselves both from a lightning standpoint and switching surges of the usual variety. This would seem fundamentally so. However, this consideration precludes oil running out of segments of the cable and assumes that vital details have been satisfactorily taken care of. Transformers connected to highly insulated wood-pole lines require more than usual attention, to provide satisfactory protection against lightning; and so does a cable under similar circumstances. It must be remembered that while a cable may provide some protection to other apparatus under certain conditions of operation, it may become a source of hazard. It has been my experience that these and a host of other applications require individual attention.

A word on impulse testing. Whether the authors have in mind a type test or, eventually, an acceptance commercial test, cable engineers would do well to avail themselves of the experience from the testing of transformers. With present impulse generators of high capacitance used in testing transformers, it should be possible to test a substantial length of high-voltage cable, that is, for example, a 230-kv or 138-kv section well up to several thousand micromicrofarads. This can be done maintaining somewhat the same tolerances for the wave, and so forth, as are specified in the transformer test code. It is hardly possible to outline here impulse-test methods for cables, but this we can say, that the experience in testing other apparatus, and in particular transformers, should be helpful in establishing an impulse test code for cables when that day comes.

Robert J. Wiseman (The Okonite Company, Passaic, N. J.): As a member of the cable working group under whose auspices this paper was written I was quite pleased when I found the authors felt it was possible to begin to draw conclusions from test data that had been accumulating for years on impregnated-paper cables. The setting up of withstand voltages and comparing them to the basic impulse insulation level for equipment may be a guide as to how much a factor of safety a cable has. If only slightly higher than the basic impulse level, then it would seem as if we have reached a limit in the insulation thickness for that particular type of insulation—for example, oil-filled cable. However, we can wonder why the withstand voltage for solid-type paper cable is at least double the basic impulse level and conclude from experience that there are other things to consider for solid-type paper cables than impulse strength, such as the change in degree of saturation with time or the possible deterioration of the insulation with temperature and time. Both may have an effect on impulse strength as well as on long-time voltage breakdown. Perhaps some day we will collect more data for further enlightenment, such as obtained by The Detroit Edison Company.

It is to be regretted that the data available on varnished cambric and rubber are quite meager. Some of us have had a few tests made, but there is room for plenty more. In view of the tests on sheets of varnished cambric as given in Figure 8, indicating this may be a method of collecting data, it might be possible that similar tests on slabs of rubber of varying thickness might result in worthwhile information. However, in these cases it seems as if we should calculate the maximum stress on the cables for the tests reported in both Figures 8 and 9 and compare to sheets and slabs.

I think the discussion of switching surges is very well done and helps us to feel that paper cables, at least, are capable of withstanding many such surges, of course, if properly made. Grant that the effects of switching surges are accumulative and approach the five-minute voltage-breakdown tests, it will take a good many such tests to reach a fatigue point of the insulation and recovery of the insulation will probably take place between surges. Here also, I hope that it won't be too long before we will have a paper dealing with latest information on switching surges, their magnitude and effect.

This paper has been needed for quite a number of years in order for us to get a composite picture of that which has been published in several places and the conclusions that can be drawn therefrom. The authors have done a splendid piece of work.

Herman Halperin and **G. B. Shanklin:** The discussions (*a*) substantiate the judgment of the cable working group that there is considerable interest in the matter of impulse strength of cable circuits and (*b*) indicate the need for further researches and for further co-ordinating work by the AIEE.

It is gratifying to learn from Mr. Atkinson that his laboratory is setting up more equipment for investigations with waves comparable to switching surges. The paper, as far as is known to the authors, takes the first really definite stand in this country as to switching-surge strength of some types of insulation, and incidentally test data on many types of equipment would be of interest.

The authors are quite in sympathy with some of the detailed points made particularly by Mr. Atkinson and by Mr. Komives, but the assignment was to collect and co-ordinate all available impulse-strength data and present them and accompanying conclusions in a paper of reasonable length and simplicity. Further refinements in analyses and in conclusions do not seem warranted until more data are available. Regarding one of Mr. Atkinson's points, the supplemental data now available are so varied and voluminous that it does not seem warranted to include them in a tabulation in the closure.

Mr. Komives' first point indicates a desire for an insulation structure that for 120-kv cable would have an average withstand impulse stress of 1,500 volts per mil or 25 per cent higher than justified by the authors' data for a general value. This does not mean that 1,500 volts per mil is unattainable, but means that special study and special developments, such as was done on the 120-kv cable installed in Detroit a few years ago, will be advisable for such circumstances; in some cases overinsulation to meet the required withstand impulse-voltage strength may be the practical solution.

It is agreed with Mr. Komives that for solid-type paper cable "service or test aging resulting in poor impregnation will decrease the impulse strength of the cable." Regarding his last point, the reference classes in Table I of the paper are practically the preferred-voltage classes of the Association of Edison Illuminating Companies cable specifications. Some of the classes he mentioned have not as yet applied to cables; however, some further co-ordination may be necessary by the AIEE.

Doctor Wiseman's discussion as well as his original encouragement to prepare the paper are all appreciated. His suggestion to make further tests on small samples seems good, and may be of immediate use in investigating some of the synthetics being discussed for wider use in cables.

It is agreed with Mr. Bellaschi that more data in drawing Figure 7 are desirable, but it should be distinctly understood that the graph is based on more data than shown in the figure. First, the references contain test data obtained with voltage applications ranging from about one microsecond up to about one cycle on a 60-cycle basis. Second, similar graphs have been presented for composite insulation of oil and paper boards as in transformers. Third, there are the years of experience with switching surges on lines.

It is agreed with Mr. Bellaschi that past experience in impulse testing of transformers is helpful and such experience was in mind in preparing section III. The main purpose of section III was to co-ordinate methods used in future tests and the reference in part 4 was to indicate that AIEE Standards should be followed on other details.

Professor Siegfried's discussion opens up interesting avenues for further investigations of the nature and breakdown characteristics of insulation. It is hoped that some such studies will be undertaken. In regard to one of his points, it was not intended that impulse tests would be a routine for cables.

Cyclic Movement of Cable—Its Causes and Effects on Cable-Sheath Life

(44-131)

An Investigation of the Relationship Between Temperature and Movement of Cables in Ducts

(44-132)

Discussion and authors' closures of papers presented at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944, and published in AIEE TRANSACTIONS, 1944: 44-131 by Clement S. Schifreen, pages 1121–30; 44-132 by C. T. Nicholson and T. J. Brosnan, October section, pages 723–8.

T. H. Haines (Boston Edison Company, Boston, Mass.): In an attempt to correlate the severity of cable-sheath troubles with racking conditions in the manholes a survey has been made of two heavily loaded double tie lines which have given considerable trouble in the past. They were installed 17 years ago in a duct line having a considerable variety of manhole conditions.

Pertinent data regarding the lines are as follows:

1. Lines 4-1213 *X* and *Y* are 3x300,000-circular-mil belted 28-kv cables with an outside diameter of approximately 2.96 inches.
2. Lines 4-1214 *X* and *Y* are 3x700,000-circular-mil shielded 15-k vcables with an outside diameter of approximately 2.96 inches.

All four cables are installed in $3^1/_2$-inch fiber ducts with section lengths averaging

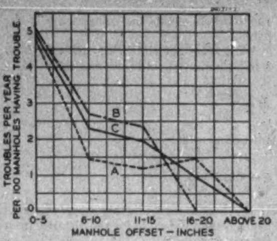


Figure 1. Expansion and contraction troubles (cable replacement and joint failures), 1927–1943 inclusive

A—Line 4-1213*X* and *Y* (two 3x300,000 circular-mil cables)

B—Line 4-1214 *X* and *Y* (two 3x700,000 circular-mil cables)

C—Combined trouble rate (four cables)

300 feet with approximately 480 feet maximum. The contour of the duct line is approximately level. The load cycles for all cables have been variable daily from less than $^1/_4$ load for two to three hours to over $^3/_4$ load for five to six hours.

In order to avoid obscuring the trend with much inconclusive data, only those troubles were included which required replacement of the cable or remaking of the joint.

To eliminate error due to a concentration of manholes in any particular offset group the number of troubles in each group was converted into a rate per 100 manholes having trouble in that group.

Figure 1 of this discussion shows the results of the survey. The trouble rates for the two lines are of the same general magnitude, particularly in the upper and lower offset ranges. Above a 20-inch offset, or about $6^3/_4$ cable diameters, neither line had a serious trouble for the 17-year period. For a longer life and avoidance of lesser troubles a considerable increase in offset undoubtedly would be necessary.

Wm. A. Del Mar (Phelps Dodge Copper Products Corporation, Yonkers, N. Y.): Mr. Schifreen tells us that tests and operating experience indicate that unsatisfactory sheath life can be caused by fatigue of lead, resulting from cyclical movement of cable.

There recently has come to my attention an extensive system of communication cables in which 90 per cent of the breaks in the lead sheaths occurred at the wiped joints and 5 per cent at the duct mouth.

The heating of these cables by current is quite negligible, so that some other cause than cyclical movement was at work. Perhaps the same cause, whatever it may have been, was at work in Mr. Schifreen's cases.

A suggested cause is vibration due to road traffic. This naturally would be most intense at the ends of the readily movable section between the more fixed joint and duct sections.

If this should prove to be an important factor, cushioning to reduce vibration might have a very salutary effect.

A. Bodicky (Union Electric Company of Missouri, St. Louis, Mo.): An attempt was made to visualize the action that takes place during the heating and cooling cycle of the cable as illustrated in Figure 2 of this discussion, which was developed by making simplified assumptions and is on a greatly exaggerated scale in order to picture the stress and movements in a cable.

It was assumed that the initial state of the cable was without stress or strain and was subdivided into unit sections with its friction concentrated at the end of the section shown as a solid line. It was further assumed that the temperature coefficient of the cable was such that for every unit of temperature rise the cable expands one small division or 10 per cent. At the same time it takes slightly more than one unit of force to move the section against the friction. Also, the compressibility of the cable is 10 per cent per unit force, or one small division. Since the movements are symmetrical about the center of a horizontal cable, only the right half of the cable length is shown.

As the temperature is raised by one unit of temperature, the whole cable will be under compression, and no part of the cable will move. This is also true of the end section, as it requires slightly more than one unit force to overcome the friction. If the temperature rise to two unit degrees, then all of the sections will be under compression with two unit forces, except the end section which will expand one division and still be under compression with one unit force. With a three-degree temperature rise, the second section from the end will expand one division, since it takes two unit forces to move the two sections to the right of it, and at the same time the end section also expands one division so that the total end movement is two divisions. As the temperature is increased further, more and more sections will move, and the end movement will become greater and greater.

Figure 2. Graphical representation of cable movement

Each unit (ten divisions) expands one division for each one-degree rise

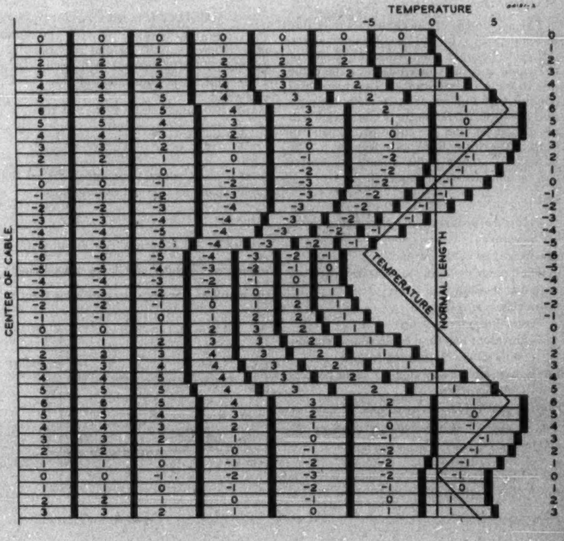
Each unit compresses one division for each unit force; friction per unit one plus unit force

After reaching the maximum temperature of, for example, six unit degrees, the cycle is reversed, and the internal stresses correspondingly reduce by one unit force in each section per degree drop in temperature. At five degrees the end section will be without stress, and the end will not move until the temperature is reduced by two more degrees. It is noted in this stage that there occurs both tension and compression in the same length of cable, as indicated by the positive and negative figures.

The same reasoning is applied for further reduction of temperature. It is interesting to note that, after reaching the original temperature of zero degrees, the expanded sections of the cable are under tensions. However, the center sections which did not move are again without stress. If the temperature is reduced further by the same amount as the rise, the total end contraction from the normal position is the same as the original expansion.

The following cycle shows that, if the temperature does not go below the original temperature, the expansion is only one fourth of the previous value, which checks the theoretical calculations that the expansion is proportional to the square of the temperature variations.

This graph also shows that, regardless of how far the center line of the cable is moved to the left, the movement will remain the



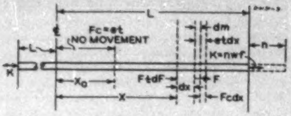


Figure 3

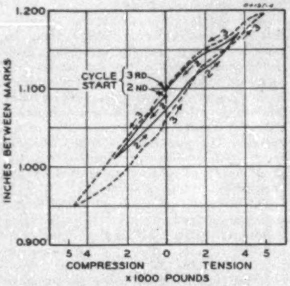


Figure 4. Second and third cycle of compression and tension on a 13-foot 4¼-inch length of 350,000-circular-mil sector three-conductor belted 23x9/64 paper-insulated lead-covered cable in a 3½-inch iron pipe

Cable manufactured in 1923
Average $C = 0.16 \times 10^{-6}$

same for the same temperature variations.

By substituting the correct constants and reducing the sections to smaller units, the movement will follow a smooth curve covered by a mathematical formula.

Mathematical Solution

The movement of the cable in a duct due to temperature rise can be considered as the difference between the thermal expansion of the cable and the compression of the cable due to frictional resistance. In a horizontal conduit with uniform friction along the conduit the cable in the center of the section will be stationary, and the cable will expand in both directions from the center. The movement at any point x from the center can be obtained from Figure 3.

Let

t = temperature rise above the average temperature
w = weight of cable per inch
L = half length of cable section in inches
c = compressibility of cable
f = coefficient of friction
k = restraining force $= nwf$
n = equivalent length of cable whose friction results in the restraining force k
m = movement at any point x from center, M, in manhole
e = thermal expansion of copper

The forces on either side of element dx $-dF = wfdx$ or $-F = wfx + a$ when

$x = L + n$

then $F = 0$ and therefore

$F = wf(L + n - x)$

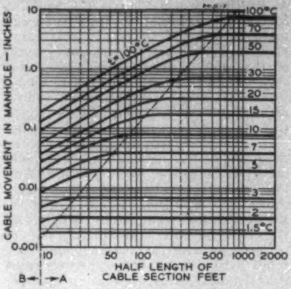


Figure 5. Movement of cable in manhole; 350,000-circular-mil three-conductor sector

23x9/64-inch paper-insulated lead-covered 33-kv cable

$$A\text{—Movement} = \frac{e^2t^2}{2wfc}$$

$$B\text{—Movement} = M = etl - \frac{wcfl^2}{2}$$

e = thermal expansion $= 16.5 \times 10^{-6}$
c = compressibility $= 26 \times 10^{-6}$
w = weight of cable = 13 pounds per foot
f = frictional constant = 1
l = half length of cable feet
t = temperature rise, degrees centigrade, or half of total temperature variation

also

$$dm = etdx - wfc(L + n - x)dx$$

or

$$m = (et - kc)x - wfcLx + wfc\,\frac{x^2}{2}$$

the integration is taken between the limits of zero or x_0 and L for the two cases required. Zero movement will occur when $x = 0$, that is, in the center of the section, and when

$$x_0 = \left(L + \frac{k}{wf} - \frac{et}{wfc}\right)$$

or at a point where the compression of the cable equals its thermal expansion.

$(Fc = et)$

With a temperature rise from 0 to t_0 when $t_0 = \dfrac{kc}{e}$ there will be no movement in the manhole, that is, the restraining force in the manhole equals the compression of the cable.

With a further rise in temperature from t_0 to t_1 the movement in the manhole is

$$M = \frac{(et - kc)^2}{2wfc}$$

where

$$t_1 = \frac{c}{e}(Lwf + k)$$

If the maximum operating temperature is below t_1 then the cable for the distance from the center to

$$x_0 = L + \frac{k}{wf} - \frac{et}{wfc}$$

will not move, and all movement will occur beyond this point to the manhole. For the same temperature variation the movement in the manhole is independent of the length of cable section.

When the operating temperature exceeds t_1 then the movement becomes

$$M = (et - kc)L - wfc\,\frac{L^2}{2}$$

A test was made on a 13.3-foot section of 350,000 circular mils three-conductor sector 23x9/64-inch paper, lead-covered 33,000-volt cable (that was removed after 20 years of service) in which the cable was compressed and elongated longitudinally in a $3^1/_2$ inch iron pipe without buckling, and the constant c was found to be 0.16×10^{-6}. Figure 4 shows the hysteresis loop for the second and third cycle. The first cycle is similar to the latter two cycles, but because of equalization of the end clamps the initial cycle may not be representative of the daily movements. From these tests the movement in the manhole was calculated and shown in Figure 5.

Since at the maximum temperature almost the entire length of cable is under compression, then when the temperature is lowered below the original or starting temperature, the cable will be under tension, and the movement will follow approximately the same formula as that for the temperature rise, except in the reverse order. During continued daily load cycles the cable ultimately will assume a neutral position,

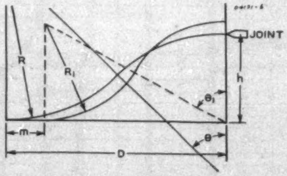


Figure 6

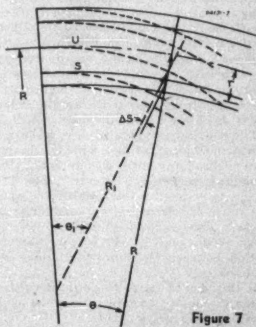


Figure 7

somewhere near the average temperature and will be alternatively under compression and expansion during the load cycles. If during the rise in temperature the cable does not buckle, then if the temperature is lowered by the amount of twice the original rise, the total movement of the cable will be twice that of the original expansion. On this basis t is to be considered as half of the total temperature variation, and the total movement in the manhole is twice the calculated value M. In other words t then becomes the variation of temperatures above or below the average temperature.

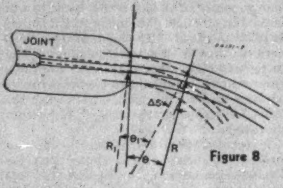


Figure 8

The compressibility of smaller cables no doubt increases at a greater rate than the reduction of the unit weight of the cable, therefore, the neutral zone x_0 should increase, and the movements in the manhole would correspondingly decrease. Buckling or snaking of the smaller cables will reduce further the movement in the manholes.

Cable Movement on a Slope

If a cable is on a slope with an angle of α from the horizontal line, then the frictional constant f should be replaced by $f \cos \alpha \mp \sin \alpha$, where $-$ sign applies during increase of temperature and $+$ for lowering temperature. As long as the center of the section does not move either during expansion or contraction, then the cable will not move downhill.

$$x_0 = L - \frac{et - ck}{wc(\cos \alpha - \sin \alpha)}$$

during expansion or as long as the temperature t does not exceed

$t_1 = \frac{c}{e}[k + Lw(f \cos \alpha - \sin \alpha)]$ no downhill movement will occur.

Cable Movement in the Manhole

The expansion of the cable in the conduit will cause a movement in the manhole, and, if the cable is trained around the manhole, the bending radii will change. Consequently the lead sheath will compress and expand both along the inside and outside of the bend, see Figure 6.

Since the length of the trained cable remains constant, that is, $R\theta = R_1\theta_1$ it can be shown that

$$D - m = 2R\theta \frac{\sin \theta_1}{\theta_1}$$

where

$$h = 2R(1 - \cos \theta)$$

and

$$D = 2R \sin \theta$$

From suitable tables θ_1 can be obtained. The lead sheath on the inside of the bend will compress Δs as shown in Figure 7.

$$\Delta s = ru\left(\frac{1}{R_1} - \frac{1}{R}\right)$$

and the compression per unit length of cable becomes

$$\frac{\Delta s}{u} = \frac{r}{R}\,\frac{\theta_1 - \theta}{\theta}$$

If this compression and expansion of the lead sheath has some relation to the life of the sheath, then the life should be inversely proportional to the diameter of the cable and directly proportional to the radius of the bend. However, if the ratio of the bending radii to the cable diameter remains constant for all sizes of cables, then the life should be independent of the diameter of the cable, except for the fact that a larger diameter of cable with its relatively lighter lead will wrinkle more readily than a small cable.

Considerable more movement of the sheath will occur near the joint and will depend upon the diameter, bending radii, and the stiffness of the conductor and the splice. If the conductor in the splice deflects only slightly, then due to the rigidity of the wipe the unit compression will increase at this point, as is evident from Figure 8. Therefore it seems advisable to construct a rigid splice or reinforce the lead sheath for a short distance beyond the splice. This point has always been a weak point on the cable sheath.

H. W. Clark (Potomac Electric Power Company, Washington, D. C.): As an example of cable movement, the behavior of a 450,000-circular-mil three-conductor type H 210-mil paper 149-mil 2 per cent tin lead sheath 40-kv oil-filled cable is presented. This cable, which has an average diameter of 2.64 inches, operates as a tie line between two generating stations, and at the time of our investigation the daily load factor was 60 per cent. It is thought that this survey may be of particular interest in that the cable is installed in a tile duct of square cross section with an inside dimension of $4^3/_8$ inches. The 5x13 foot rectangular-shaped manholes, through which the cable passes, are 67 in number. The results of the survey are shown on the accompanying tabulation.

Consideration of these data, together with an examination of the entire profile of the cable run, including sections where cable movement was and was not observed, would indicate that it is impractical to predetermine where movement will occur. This is due to the number of variables that affect the phenomenon of cable movement. The study indicates that at least the following apply:

1. Per cent grade of duct run.
2. Load cycle on cable.
3. Configuration of duct run.
4. Relative elevation of ducts entering manholes.
5. Wedging of cable due to snaking.

The field inspection indicates that there is no consistent relation between per cent grade, length of duct run, and cable movement. In the two sections between manholes 21–22 and manholes 33–34, the cable apparently moved uphill. No movement occurred in section 45–46, although this stretch had a considerably greater slope than the adjacent sections 43–44–45 where movement did occur.

That load cycles affect the movement of cable in the duct run is apparent and hardly requires demonstration. However, this is borne out in this case in the numerous instances where cable moved into one end of a duct run and did not emerge from the other end, indicating that the movement is caused by snaking from heat effects and not by slippage.

Due to the fact that the profile can be checked only at the points where the conduit enters and leaves the manholes, the actual path of the duct run between man-

Table I. Cable Movement of Number 901 40-Kv Feeder—Benning to Buzzard Point

Movement				Per Cent Grade	Section Length, Feet	Cable Movement	Grade Direction
Manhole	In.	Manhole	In.				
18	4	17		2.77	412.0	18–17	18–17
17	3	16	3	2.12	264.0	17–16	17–16
16	3	15		2.36	445.0	16–15	16–15
15	3	14	3	2.76	272.0	15–14	15–14
14	3	13	3	2.14	289.0	14–13	14–13
21	5	22	2	1.16	387.5	21–22	22–21
22	2	23	1	1.28	373.6	22–23	22–23
28	3	29	3	1.19	210.3	28–29	28–29
35		34	2	0.00	307.2	35–34	
34	2	33		0.74	402.3	34–33	33–34
43		44	2	2.76	145.1	43–44	43–44
44	2	45		3.47	175.8	44–45	44–45
48		49	6	1.75	269.1	48–49	48–49
49	6	50		0.86	406.9	49–50	49–50
52		51	8	1.59	207.4	52–51	52–51
54	2	53		1.15	277.3	54–53	54–53
63		62	4	1.32	412.5	63–62	63–62
65		66	4	1.10	462.0	65–66	65–66
66		67	?	0.30	462.0	?	67–66
67	?	68	2	0.83	348.6	67–68	67–68
68	2	69	2	1.56	38.5	68–69	68–69

holes is not known. Construction specifications require that all duct runs shall drain toward the manholes, but this can be done all in one direction or in both directions. The actual path would be determined by obstructions and economy in excavation. Cable movement would be affected by the different frictional resistances thus set up.

In co-ordinating the movement with the profile, it was noted that in several instances the movement ended in manholes where there was considerable difference in elevation between the duct entering and leaving the manhole, resulting in a combination vertical and horizontal radius in the cable which aided in absorbing the movement.

Movement uphill probably is explained by the cable wedging itself at some point in the duct run as a result of snaking during a load cycle. On subsequent load cycles it is forced to move toward the higher duct entrance.

That a cable under a given set of conditions will exhibit a certain movement is shown by the fact that a similar cable operating in parallel with the cable under investigation, but racked on the opposite side of the manholes, exhibited practically identical movement.

Charles F. Avila (Boston Edison Company, Boston, Mass.): For cable reinstalled after troubles due to cable movement in existing manholes which cannot economically be enlarged, flexible inserts consisting of brass bellows have been installed in the sheath. When it is desired to repair a break in an existing cable, flexible inserts of Thiokol tape are installed; 35 sets of bellows and 292 sets of tape inserts have been installed to date. Since the majority of these have been in service only two or three years, a definite statement of their value, based on field experience, cannot be made. In the laboratory nine cable samples without inserts failed after 730 to 11,075 cycles of bending at a rate of 8 to 12 cycles per minute on a machine having a two-inch stroke. A large number of samples having Thiokol inserts applied in various ways were tested, the better specimens withstanding 6,200 to 91,948 cycles on the same machine. Six samples with bellows installed lasted from 7,149 to 75,060 cycles, and one was still airtight after 101,802 cycles. The latter sample, when subjected to a high-voltage time test with visual examination, indicated that no serious damage to the insulation had occurred as a result of the bending.

One test failure under bellows has occurred as a result of a leaky wipe and one service and one test failure because of moisture, which we believe was present in the old section, undetected, before the bellows was installed. Two failures have occurred in Thiokol-protected sections for the same reason, and two other failures near taped inserts resulted when the sheath opened at an old solder patch. The sheath, in question, previously has been weakened at such points, but manhole dimensions do not always permit the installation of a taped insert long enough to absorb sufficient bending for complete protection.

It is not believed that either metal bellows or Thiokol inserts are the equivalent of new cable installed with adequate racking conditions. Where the alternatives are pulling a great many sections of cable or the constant patching of cracks with the probability of numerous failures, the use of bellows or inserts appears to have considerable economic merit.

Herman Halperin (Commonwealth Edison Company, Chicago, Ill.): Mr. Schifreen's paper adds voluminous data to the picture showing that fatigue of sheaths due to daily movements incidental to load-cycle changes easily may be the limiting factor in loading up underground power cables. For instance, the author makes an important statement in reaffirming the fact that short sheath life is obtained in the regular daily loading of cables up to merely the temperatures allowed in standard specifications which include the limits set by the AIEE Standards.

A comparison of Chicago and Philadelphia data and Mr. Schifreen's data shows that for a temperature rise of 20 or 25 degrees centigrade, his 350,000 circular mil, three-conductor 13-kv cable has approximately one third less movement at the duct mouth than does the Commonwealth Edison's 500,000-circular-mil three-conductor 12-kv cable. The cable discussed by Mr. Schifreen has smaller conductors, making it less stiff than Commonwealth Edison's, and the latter's studies on various cables show that this factor would explain part of the difference. The former cable has more clearance in the duct than does the latter, and the other data show that snaking in the duct helps reduce the movement at the duct mouth.

Schifreen's data agree with the Chicago data in showing that there is a limiting cable length above which further increases in cable length do not cause additional increase in movement. For the temperature range he has studied, we agree that this critical length is about 300 feet. For extra high temperature ranges such as would occur during rare emergencies, the Chicago data show that the critical lengths may be as much as 400, 500, or 600 feet. However, Chicago studies do not indicate any great loss of life due to rare large cable movements that would occur incidental to emergency loading.

Figure 6 of Schifreen's paper does not confirm his conclusion number three that cable movement is a linear function of the temperature rise; instead it shows that the movement at the duct mouth increases at a rate greater than the first power of the temperature rise.

The author's discussions on the theory of sheath strains in appendix A and in connection with extrapolating Professor Moore's data in Figure 13 are interesting and ingenious, but seem to rest partly on dubious grounds. The assumptions, for example, of a fictitious modulus of elasticity for the cable and that all bending occurs at points *A* and *C* are questionable. Worse yet is the apparent assumption that the strain is independent of the diameter of the cable. In cables in manholes the lead sheath is entirely in compression on one side and in tension on the other side of the cable, whereas in Moore's tests the thickness of the strip is partly in compression and partly in tension.

Schifreen's data on dummy-manhole cable-movement tests with the sheath heated are very valuable; they show a one-third reduction in sheath life at 35 to 42 degrees centigrade as compared with sheath life at room temperatures of 18 to 25 degrees centigrade. In my 1942 AIEE[1] paper, I reported a one-half reduction between laboratory and field tests, which includes the effect of temperature.

In connection with his data on sheath strain, I should like to emphasize the tremendous benefit of having adequately large offsets between the cable at the duct mouth and at joint. This point was brought out in the test and field data in my AIEE papers in 1939[2] and 1943.[3] The point also has been demonstrated with a vengeance, according to reports I have heard recently from five cities on how many sheath cracks occurred on some lines having small offsets when the loads became moderate to heavy, but within the ratings.

A recent Chicago survey on the effect of loading on sheath cracks shows that for 500,000 circular mil, three-conductor, 12-kv cable the rate of sheath cracks per 100 miles of cable was seven times as much for lines having daily maximum loads of 275 to about 375 amperes as for lines carrying from less than 200 amperes up to 275 amperes.

It was good to see the data based on a cycle in the dummy-manhole tests longer in time than that used in Chicago. Our time of 70 seconds was evolved many years ago, and much data have been accumulated on that basis. If we had started fresh in our studies within the past few years, then, based on our information which includes data from others, I would have advocated the use of a cycle even longer than Schifreen's, that is, six minutes.

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3. GUIDE FOR WARTIME CONDUCTOR TEMPERATURES FOR POWER CABLES IN SERVICE, Association of Edison Illuminating Companies Subcommittee on Wartime Temperatures for Cable Circuits, AIEE TRANSACTIONS, volume 62, 1943, September section, pages 606–10.

M. D. Hooven (Public Service Electric and Gas Company, Newark, N. J.): Mr. Schifreen's treatment of his subject has been so complete that it is difficult to add to it. However, I have some pictures of experiments conducted with a different type of cable, which should add to the interest in the general subject.

As a part of a Public Service Electric and Gas Company investigation of causes of failure in large rubber-sheathed generator cables, a number of expansion experiments were conducted. Two parallel four-inch Transite ducts were laid on the ground, their tops cut off for purposes of observation, and a 1,800,000-circular-mil varnished-cambric static-sheathed rubber-belted cable was installed in each duct. One cable was clamped at each end, and the other was left free to move. The cables were loaded and the unrestricted cable moved freely out of the ends of the duct with practically no waving in the duct.

The restricted cable started a visible waving movement at 23 degrees centigrade with nodes forming every 16 feet; as the temperature approached 36 degrees centigrade, the nodes doubled and doubled again at 55 degrees centigrade at which time the nodes were about four feet apart. A further increase in temperature resulted in the cable being forced against the top of the duct with considerable pressure. It was noticed that, prior to the formation of the additional nodes, the cable between nodes was under heavy compression.

Some cable was then installed in a six-inch rectangular duct, and temperature runs made at 23.3, 33.3, 43.3, 53.3, 63.3, and 73.3 degrees centigrade. As was to be expected, with increase in temperature snaking increased and instead of the cable rising as occurs in a duct of circular cross section, all the movement took place horizontally. Figure 9 shows the position of the cable in a duct at the beginning of the test, which for all practical purposes, may be considered straight. Figure 10 shows the final position of the cable after the run at 73.3 degrees centigrade. Note the very definite snaking of the cable to take up the increase in longitudinal length due to expansion.

Figure 9. Essex station—100-foot 1,800,000-circular-mil cable anchored both ends in rectangular duct 5$^3/_4$x4 inches at 3.3 degrees centigrade

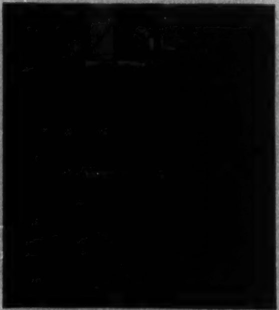

Figure 10. Essex station — 100 - foot 1,800,000-circular-mil cable anchored both ends in rectangular duct 5x4 inches at 73.3 degrees centigrade

These tests showed very clearly what takes place out of sight in actual service. Such studies as these indicate to us that it is well worth studying the part that the type of duct plays in cable operation.

Mr. George Dolan, the engineer conducting the tests, had a peculiar experience during the first experiment. When the cable was waved with nodes about eight feet apart, he pressed firmly on the cable at a point where it was pressing against the top of the duct. With a sudden snap the whole cable took a new wave formation with nodes about four feet apart. This occurred at the first heating cycle; after that, as Mr. Schifreen has pointed out in his paper, the cable takes a definite set and always waves at the same four-foot intervals.

G. B. McCabe (The Detroit Edison Company, Detroit, Mich.): Elongation measurements were made by The Detroit Edison Company on a number of belted- and shielded-type cables incidental to determining the maximum ratings for 2/0 American Wire Gauge and 350,000-circular-mil 24,000-volt cables with loadings which might obtain under emergency conditions in service.

Since the Buffalo tests were conducted on 350,000-circular-mil 25-kv shielded cable, Table I following shows the elongation data taken on only new 350,000-circular-mil shielded cable when under high emergency loads. The cable was subjected to several normal-load cycles, followed by one emergency-load cycle in which the current obtained in corresponding steps of the normal load-cycle was doubled. This sequence was repeated five times, and the values shown in the table represent the maximums attained at the end of each cycle for both elongation and sheath temperature. The cable under test was placed in an outer duct (bank three ducts wide and four high), and six of the outer ducts were loaded artificially, four to duct-air temperatures of 35 degrees centigrade and two to 45 degrees centigrade. A report on these tests is given in *Electrical World*[1] and the *Edison Electric Institute Bulletin*.[2]

Comparing the elongation in inches per 100 feet per degree centigrade on the various tests made on the Buffalo cable between Manholes 347 and 317 and the Detroit cables under much higher sheath temperatures, a reasonably close agreement is found for the values of the coefficient.

The data obtained support in part observation 3 in the Buffalo paper, that is, the amount of longitudinal expansion appearing in the manholes was less than one half of the theoretical free longitudinal expansion for the lead sheath. This figure, however, compared to the theoretical longitudinal expansion of copper was found to be greater than one half for the high copper temperatures attained in the 15-hour test, but less than one half in the 3$^1/_2$-hour test.

Table II

Test	Load: Amperes	Load: Duration, Hours	Sheath Rise, Degrees Centigrade	Elongation, Inches Per 100 Feet Per Degree Centigrade Rise
Buffalo manholes 347-317				
A	260	4	12.0	0.0091
B	260	4	13.7	0.0097
E	260	4	12.4	0.0098
F	260	4	10.8	0.0101
I	260	4	10.2	0.0107
M	260	4	12.0	0.0116
				0.0102 Average
G	300	4	14.7	0.0107
K	300	4	14.4	0.0101
N	300	4	16.5	0.0121
				0.0110 Average
H	380	4	22.9	0.0142
L	380	4	23.1	0.0146
O	380	4	24.4	0.0148
				0.0145 Average
C	325	72	37.0	0.0132
D	300	149	41.0	0.0134
Detroit data				
1	300	15	38.0	0.0130
2	520	15	73.4	0.0147
3	535	15	88.2	0.0141
4	600	15	108.0	0.0147
5	565	3$^1/_2$	85.4	0.0114
6	600	3$^1/_2$	104.0	0.0110

The limited data did not, however, support observation 2, that the average longitudinal expansion appearing in the manholes per degree of increase in cable temperature was greater for the larger increases of cable temperature. No correlation could be noted between the two in the Detroit tests. For example, the same coefficient of expansion or movement, namely, 0.0147, was obtained in test 2 with only 73.4 degrees centigrade temperature rise as in test 4 with 108 degrees centigrade rise. Test 6 had a smaller coefficient, namely, 0.0110, with a rise of 104 degrees centigrade than test 5, in which the coefficient was 0.0114 with a rise of 85.4 degrees centigrade.

The duration of application of a given load appears to be a factor influencing the value of the coefficient of movement. For example, in Detroit tests 4 and 6, it will be noted that with the 600-ampere load applied for 3$^1/_2$ hours the coefficient is but 0.0110, whereas the 15-hour test gives a coefficient of 0.0147. Likewise, in the Buffalo tests *G*, *K*, and *N*, the application of 300 amperes for four hours gives an average coefficient of 0.0110, whereas 300 amperes applied for 149 hours in test *D* and 325 amperes for 72 hours in test *C* results in coefficients of 0.0134 and 0.0132, respectively.

Although the use of the metal bellows insured a positive contact of the thermocouple against the lead sheath, is not it possible that the increased radiating surface of the bellows might radiate heat and thus affect the reading? Likewise is not there a probability of the steel pipe from the manhole wall to the sink conducting heat to or from the ambient-temperature location?

It is noted that in all these tests the fireproofing was removed from the cables. Would not it have been desirable to have conducted some tests on cables as they are fireproofed normally in field installations in order to determine the restraining or stiffening effect of fireproofing upon cable movement?

In the paper under discussion, there is a statement that the maximum temperature rise of each section is at the end of maximum elevation, which does not appear to be true of curves *A* and *C* between manholes 205 and 247, and of *C* and *D* between manholes 347 and 379 on Figure 7.

There also appears the statement that the realized longitudinal expansion per unit length of cable is less for a long section than for a short section. Was not this smaller expansion per unit length on the longest section, namely, 205–247, caused by the smaller increase in sheath temperature?

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2. Results of Emergency Overload Tests on 24-Kv Three-Conductor Paper-and-Lead Cable, G. B. McCabe, Joseph Sticher. Edison Electric Institute Bulletin, November–December 1942, pages 405–09.

Clement S. Schifreen (Philadelphia Electric Company, Philadelphia, Pa.): The authors, C. T. Nicholson and T. J. Brosnan, have contributed some data on 27-kv, three-conductor 350,000-circular-mil cable to a gradually accumulating reported record on cable movement at duct entrances in manholes for various conductor sizes and voltage ratings. These data, however, are in terms of lead-sheath temperatures six to seven feet up the duct from the manhole. Mr. Halperin in his paper, "Load Ratings of Cable,"[1] shows about a five degrees centigrade difference in empty-duct temperature alone between this point and distances further up the duct. Our experience with field-temperature surveys also demonstrates this influence of ambient air in the manhole on the temperatures of empty ducts and operating cables close to the duct entrances. It is therefore difficult to compare the present results with data previously reported on the basis of conductor-temperature ranges, computed by accepted methods to reflect effectively the conditions of the entire section.

A theoretical analysis of the mechanism of cable expansion is also given. Although we also originally had entertained some of these conceptions, it was found advisable to reconsider them in the light of our test results. For example, Figure 11 (left) in this discussion is representative of the positions assumed by cables when initially drawn into a duct. The cables were installed by a regular cable crew using field methods. A tension representative of the leading end of a 600-foot section was applied to each cable. We consequently have abandoned the idea that a cable lies in a "straight path along the bottom of the duct." Figure 11 (right) shows the same cables when heated to a 40 degrees centigrade conductor temperature rise above a 20 degrees centigrade ambient. Figure 19 in my paper "Cyclic Movement of Cable," also shows that cables, after a heating cycle, fail to return to their original position when cooled to ambient temperature. The initial straight line in this figure is merely the reference center line of the cable, which was not actually straight. Snaking in Figure 11 of this discussion was mostly sidewise, whereas that in Figure 19 of my paper was chiefly in one major vertical deflection.

Figure 12 in this discussion is submitted in view of the authors' theoretical assumption that "initial increase in the length of cable is taken up principally by the lateral displacement or snaking of the cable within the duct, since the resistance of this snaking is less than the resistance to the longitudinal motion." Our test data show that the forces involved in the heating of the cable are at first effective in overcoming friction. This figure applies to a test run with 500 pounds restraint at the duct entrance in addition to the frictional forces along the cable. Horizontal and vertical snaking is seen to be negligible. Furthermore, Figure 19 of my paper shows that horizontal movement is a necessary component of snaking, which increases considerably when movement at the duct entrances are prevented. This requires larger restraining forces at the duct entrances (Figure 16 of my paper).

The authors emphasize differential expansion of lead sheath and copper conductor as a factor in producing cable expansion and in wrinkling the lead sheath at bends in the cable and at splice wipes. Tests in our dummy manhole developed exactly the same circumferential sheath cracks and ripples of the sheath at the joint wipes found in service. In these tests there were no long section lengths from which expanded lead sheath could have accumulated at the wipe. Our tests merely suggest that ripples at the wipe or at bends in the offset result from stretching of the lead by the stresses at these points while the joints move through the test or operating cycle. These ripples occurred only on sheaths maintained at temperatures of about 40 degrees centigrade during the test period of about 5,000 cycles lasting about two weeks. They did not occur on sheaths which were tested at room temperature. This difference appears to reflect the great reduction in the ability of lead to withstand stresses at elevated temperatures.

Figure 11. Snaking of 15-kv three-conductor 350,000-circular-mil semisector belted paper-and-lead cable (outside diameter—2.5 inches) in $4^1/_4$-inch terra-cotta ducts

(Left) Cables as installed in ducts at room temperature. (Right) Same cables when heated to a 40-degree-centigrade rise in conductor temperature

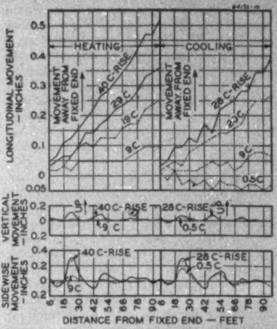

Figure 12. Movement of 15-kv three-conductor 350,000-circular-mil compact-sector belted paper-and-lead cable (outside diameter —2.24 inches) in $4^1/_4$-inch terra-cotta ducts

The upper curves show for four values of conductor-temperature rise how the cable moved along the duct when firmly fixed at one end and restrained by only 500 pounds at the other end

REFERENCE

1. Load Rating of Cable. Herman Halperin. AIEE Transactions, volume 58, 1939, October section, pages 535–53.

I. A. Rohrig (The Detroit Edison Company, Detroit, Mich.): The authors, C. T. Nicholson and T. J. Brosnan, are to be commended for a very practical investigation of the movement of lead-sheathed power cable resulting from increase in sheath temperature. Movement of cable sheath is of particular significance, because it has an important effect on the life of the sheath, not only of underground cables but also of aerial cables, and my comments on the authors' paper will be concerned with this effect as it concerns the latter type of cable installation.

A study was made recently by The Detroit Edison Company of cracked sections adjacent to wiped joints in 24-kv sector shielded antimony-lead-sheathed aerial power cable. The sheath at many of the joints in the line had bulged and cracked as shown by Figure 13 of this discussion. Most of the cracking had occurred between the bulge and the edge of the wiped joint as shown by Figure 14.

From our study it was concluded that the bulging and cracking were caused by longitudinal strains on the sheath. Such longi-

Figure 13. Typical cracks and bulge in lead sheath of aerial power cable adjacent to wiped joint in service approximately seven years, one-third actual size

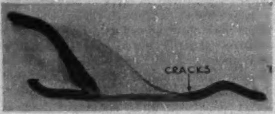

Figure 14. Cracks adjacent to a wiped joint

tudinal strains, or cyclic stressing of the sheath, are believed to be caused by expansion and contraction stresses on the sheath which result from variable loading on the cable, which in turn causes changes in sheath temperature. Atmospheric temperature change was also a factor in increasing the stress intensity on the cable sheath. In warm summer weather the cable under discussion has been known to snake in much the same way as a cable in a duct, and at times sections of the cable have risen at the side of the messenger until they were in the same horizontal plane as the messenger instead of being suspended from it. This gives some indication of the intensity of the longitudinal thrust on the ends of the joints under certain conditions of operation. Furthermore, because of their weight, the joints had sagged below the center line of the cable with the result that the longitudinal stress became a bending stress at the joints.

Secondary causes of the sheath failures adjacent to the joints were:

(*a*). Scraping of the sheath before wiping the joint. This reduces the cross section of the lead sheath and results in a weakened section in which bulging and cracking later occur.

(*b*). Stiffness of the joint. Since the joint is relatively unyielding, the longitudinal strains on the sheath are concentrated in the area adjacent to the joint.

(*c*). Metallurgical changes in the structure of the lead sheath. This is manifested as an agglomeration of the antimony constituent. This effect is accelerated by temperature and stress and results in lowering the strength of the sheath.

There was no indication that cracking of the sheath was due to annealing or softening of the sheath as a direct result of making the joints.

Since it is improbable that temperature stressing of cable sheath, particularly aerial cable, can be eliminated, it becomes necessary to design the cable line so that damage to the lead sheath can be prevented. This might be done either by redesign of the joints so that the longitudinal strains on the sheath will not be localized in the areas adjacent to the joints, or through the use of carefully placed expansion bends to minimize the effect of longitudinal strains on the sheath.

Robert J. Wiseman (The Okonite Company, Passaic, N. J.): It is with considerable relief, if not pleasure, that a time comes when a cable manufacturer can read a paper dealing with the operation of power cables and find no comments on the shortcomings of the insulation; nor in fact of the quality of the lead sheath also. It speaks well for that which has been accomplished in the technique of cable manufacture, but it also tells us that the utility engineer still has operating troubles which are noninherent but are truly present.

Both Mr. Schifreen's paper and that by Nicholson and Brosnan should be studied carefully by all operating engineers, and the information reported compared to their own experiences. We know that all utilities have experienced cable failures because of cable movement. Some companies have made investigations to determine the causes and have reported same, but we are fortunate in having these two companies describe the well-thought-out and thorough investigations made by them and give us an insight as to what can happen, if proper care is not taken in designing manholes and in the training of the cables in the manholes, and what might be done to reduce the amount of cable movement into a manhole without causing damage to the cable in the duct.

It is fortunate that Mr. Schifreen made both dummy-manhole and dummy-duct tests at elevated temperatures, as we know that temperature greatly influences the fatigue properties of lead. About two years ago we started bending tests on strips of lead in order to determine what influence the composition of the lead may have on its ability to withstand bending. We endeavored to bend strips of lead somewhat similar to the bending of a cable until they broke. The strips broke in either of the bent sections and always showed an intercrystalline fracture similar to that of cables failing in service and as shown in Figure 4 of Mr. Schifreen's paper. Figure 15 of this discussion shows the layout. We have obtained some interesting information. Tests at 110 degrees Fahrenheit (43.5 degrees centigrade) gave values about 60 per cent of the values at room temperature, and at 150 degrees Fahrenheit (65.5 degrees centigrade) they were about 60 per cent of the values at 110 degrees Fahrenheit. As cable sheaths are operating in this temperature range, we readily can see the prospects of a shortening of cable life, if cables are operated through a wide temperature range.

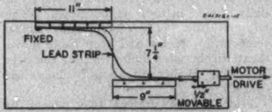

Figure 15. S-strip bending test

Three cycles per minute
One-half-inch longitudinal movement
Test made at 110 degrees Fahrenheit and 150 degrees Fahrenheit

The description of the results on restraint of a cable in Mr. Schifreen's paper is quite interesting. Why was it so, that as the paper states: "The reversed force was needed to prevent the cable end from retracting into the duct"? Had the ambient temperature dropped below its value when the cable was first installed, or did the cable snake in the duct and cause a permanent wave along its length?

Reference to friction in the duct along with Figure 6, which indicates that there is no difference in the amount of cable movement for duct lengths of 300 feet and over, may be partially explained by the kind of surface of the duct over which the cable slides. During our studies with *S* strips we noticed that we obtained varying results according to the kind of a surface we used as a plate for the sample to slide over. In the beginning we used steel and obtained a low number of cycles, 9,000–10,000 at room temperature. Changing to a sheet of varnished cambric increased them some. We finally changed to glass and obtained the values given in Table III of this discussion. For comparison we now are making tests with pressed wood to duplicate partially a fiber duct and a cement block made for us by the Commonwealth Edison Company as a duplicate of their concrete duct. The glass plate may be considered comparable to a glazed-tile duct. At 110 degrees Fahrenheit the glass plate gave high values and no difference between greasing and not greasing the surface of the plate with an increase of about 3,000 cycles when the strip also was greased. At 150 degrees Fahrenheit there was a decrease in the number of cycles when both plate and strip were greased, showing the effect of temperature. The pressed wood showed 70 per cent of the values for the glass plate and almost no difference for either side when no grease was used, but an increase of 11,500 cycles when the wood was greased, bringing it up to the values obtained for the glass at 110 degrees Fahrenheit with the plate greased. The values for the cement block at 110 degrees Fahrenheit when no grease was used are about the same as for pressed wood, and, when the block was greased, there was an increase of 10,000 cycles, bringing it up to the same number as for the glass and for the pressed wood when greased. This seems to indicate that, if there is sufficient grease on a cable to act as a lubricant, there is no difference in friction for a duct of any material, but, if there is not sufficient grease, then it is possible that there could be a restraint of the cable in the duct, and this might have an influence on the life of a cable.

The degree of snaking of a cable in a duct will depend upon the ability of the cable to snake. If there is restraint of a cable at the duct mouth and plenty of space in the duct, then one would expect snaking. It seems as if we should want snaking, provided it does not result in injury to the cable in the duct, in order to reduce the amount of cable movement. However, it is necessary to make further studies before we actually block the cable at the duct mouth to prevent cable movement. It would seem as

Table III. S-Strip Bending Tests

Sample Number	Temperature, Degrees Fahrenheit	Type of Surface	Number of Bends to Failure	Remarks
1	110	Glass plate	22,245	No grease on plate and no grease on strip
2			24,645	
			Average 23,450	
1	110	Glass plate	22,151	Grease on surface of plate and no grease on strip
2			24,235	
3			23,895	
4			24,220	
5			25,250	
			Average 23,950	
1	110	Glass plate	27,660	Grease on surface of plate and grease on lead strip
2			26,441	
3			23,994	
4			29,976	
			Average 27,020	
1	150	Glass plate	17,644	Grease on plate and grease on strip
2			16,441	
			Average 17,040	
1	110	Pressed wood	16,203	No grease on wood and no grease on strip
2		Rough side	15,897	
			Average 16,050	
1	110	Pressed wood	16,892	No grease on wood and no grease on strip
2		Smooth side	16,983	
			Average 16,940	
1	110	Pressed wood	29,621	Grease on wood and no grease on strip
2		Smooth side	27,481	
			Average 28,550	
1	110	Cement block	16,896	No grease on block and no grease on strip
2			18,421	
			Average 17,660	
1	110	Cement block	26,488	Grease on block and no grease on strip
			28,841	
			Average 27,660	

though we are forcing the cable to do that which is wanted rather than provide means for letting it do so itself. For example: a square duct would permit a cable to snake easily horizontally, whereas a circular duct, which is commonly used, makes the cable try to climb up the side of the duct and finally the cable lifts itself in order to take up the expansion. About 12 years ago I suggested for three conductor cables that the conductors be permitted to expand radially to take up the longitudinal expansion. It takes only a few mils of radial expansion to do so. A few years ago a utility tried this idea out on some 27-kv oil-filled cable. I hope some day that they will give us a report on that which they found actually to occur.

The theory advanced by Mr. Nicholson for cable movement is plausible and does explain some types of cable movement, but I would hesitate in entirely following his suggestions. If the tests carried out by Commonwealth Edison on belted and type *H* cables have any significance, a strong bond between core and cable sheath will shorten the life of the cable sheath. My aforementioned suggestion of permitting the three conductors in a cable to expand radially is a possible solution of the problem. For single conductor cables we would have to loosen up on the tightness of the wrapping of paper tapes to permit the strands of the conductor to expand radially. For low-voltage cables this could be done, but for high voltages it would not be practical because we would lower the electric quality of the cable.

This same problem has occurred in England on cable buried directly in the ground, where the cable was restrained from movement at bends, and failures resulted. A solution was obtained on three-conductor cable with a loose lead sheath and a flexible connection at the joints for the conductors. For single-conductor cable it has been proposed to wrap over the conductor a spiral tape somewhat like a *BX* tape, which would permit the conductor to expand radially and "snake" a bit inside the spiral tape without upsetting the insulation. I do not know if the idea ever was carried out. It has merit but increases the cost of a cable.

G. B. Shanklin (General Electric Company, Schenectady, N. Y.): This question of sheath fatigue from cable movement leads logically to the two papers by Schifreen, and by Nicholson and Brosnan. I fully agree with Mr. Schifreen's conclusion that present Association of Edison Illuminating Companies copper temperature limits can lead under certain conditions to sheath-fatigue troubles. This applies particularly to cable rated at 23 kv and below. The maximum allowable copper temperature in this voltage range is relatively high and is not limited by a corresponding allowable daily temperature range. Unless very liberal manholes and expansion bends are provided for, sheath troubles can, and have, occurred within the present limits. It would be more logical to reduce maximum allowable copper temperature on a graded scale for daily load factors less than 100 per cent. One simple and practical way of doing this would be to follow European practice and base all load ratings on 100 per cent load factor only, regardless of the actual daily load factors encountered. This automatically would control daily copper temperature cycles and keep cable movement within safer limits without the need of paying further attention to daily load cycles.

Prior to the present two papers by Schifreen, and Nicholson and Brosnan, there was very little published data on cable movement. Their contributions are correspondingly valuable, and it is hoped that these gentlemen will continue their studies and make the results available. At present it is very difficult to draw reliable conclusions about those factors that govern cable movement. We do know that the following factors are important, but we do not know their relative weights:

1. Temperature range during daily load cycles.
2. Manhole size and provisions for cable and joint movement in the manhole.
3. Clearance in ducts.
4. Friction coefficient of ducts and other restraining forces.
5. Ratio of cable flexibility to weight.
6. Snaking in ducts and total length of cable between manholes.

There are other factors involved, but the problem is quite complicated and not yet fully understood. This doubtlessly explains why there are so many different theories, some of them conflicting, about the best methods of controlling cable movement and the best designs of cable for this purpose. Present standard designs of cable have been proved in service, and a manufacturer would hesitate to change these designs without clean-cut evidence that such changes actually would solve the cable-movement problem. Theoretically, a more flexible cable would, if this flexibility could be carried far enough, allow cable movement to be taken up by snaking with lesser movement in the manholes. A more flexible cable, however, would mean a cable of looser cross-sectional construction, and one of the first principles in high-voltage cable design is to make the insulation cross section as compact as possible.

In a like manner, there are other conflicting requirements in some of the present theories relating to cable movement and its amelioration. Systematic studies, such as those being carried out by Schifreen, and Nicholson and Brosnan eventually will clear up these questions. In the meantime, a close control of daily load cycles and liberal manhole expansion bends offer a reliable solution.

C. T. Hatcher (Consolidated Edison Company of New York, Inc., New York, N. Y.): Two very interesting and timely papers have been presented on cable movement by C. S. Schifreen, and C. T. Nicholson and T. J. Brosnan. The expansion of cable is one of the most important things which we have to consider with the operation of cable. As stated by Mr. Schifreen the majority of cable failures today are associated with lead-sheath rupture caused by either inherent defects, corrosion, mechanical damage, or cable movement. Inherent defects lie within the manufacturers control; corrosion and mechanical damage can be controlled largely by the user of the cable; while damage to the sheath by cable movement is the common problem of both manufacturer and user. It is important that the manufacturer give serious consideration to a cable design that will provide minimum longitudinal expansion and that the users provide adequate conduit and manhole space for proper installation conditions.

Mr. Schifreen in his paper, has presented data on laboratory tests on the life of lead sheaths when subjected to movement under load conditions, and on how the life is correlated with manhole racking conditions. For the past five years we have had under study, both in laboratory and in the field, the question of longitudinal cable expansion and the factors affecting it. From our data we corroborate a number of Mr. Schifreen's findings as follows:

1. Total movement of a section generally is not divided equally at the two duct mouths.

2. There appears to be no perceptible lag between cable movement and change in conductor temperature.

3. On a heavily loaded feeder with adverse racking conditions in the manholes buckles and breaks occurred at the edge of the joint wipe.

The formula evolved by Mr. Schifreen appears to be one that will be of great assistance in determining the life of lead sheath in a manhole. On a three-conductor 800,000-circular-mil cable, having an overall diameter of 2.7 inches, which was heavily loaded, buckles and breaks in the lead sheath occurred in elapsed time from several months to two years after the heavy load was applied, resulting in cable movement. If the formula is used with L as 33 inches, cable movement as 0.55 inches, and offsets of 12.15 inches and 4 inches, values of 4 years and 1.5 years of sheath life are obtained from Curve II of Figure 9. These values check very closely with actual life.

Mr. Schifreen's statement in reference to reduction in sheath life due to snaking of the cable in the duct is important, as this has been considered as a means for reducing expanson into manholes. This point certainly warrants further investigation.

Mr. Nicholson and Mr. Brosnan in their paper have presented a very interesting point concerning cable elongation, namely the lead-sheath expansion as affected by change in earth temperatures.

Assuming that cable movement caused by sheath expansion due to earth temperature changes is absorbed by snaking in the duct, then adequate space between cable and duct wall is very important. Cable expansion caused by change in copper temperature is then reflected in longitudinal movement, after all free space for snaking has been used up. To lessen this longitudinal expansion due to change in copper temperature it would appear that a cable designed for radial expansion is desirable.

The authors statement that one millimeter of longitudinal expansion occurs per degree centigrade sheath-temperature rise should be used as a design factor has been checked from measurements made on cable operating on our system. Data was obtained on two different types of installations.

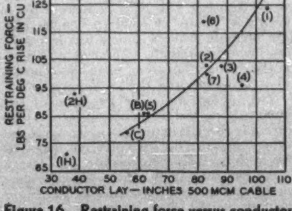

Figure 16. Restraining force versus conductor lay

Loading—600 amperes

Test 1

Three-conductor 800,000-circular-mil cable 2.7 inches in diameter in a duct approximately 3.25 inches inside diameter. Feeder was carried through cycle of no load to full load, and cable expansion was measured on two sections. One section measured 2.15 millimeters per degree centigrade sheath rise and the other 2.06 millimeter per degree centigrade sheath rise. Corresponding joint movement was 0.009 and 0.035 inch per millimeter of cable movement.

Test 2

Single-conductor 1,250,000 circular-mil cable 2.98 inches diameter in a duct four inches inside diameter. Feeder was carried through cycle of no load to 85 per cent full load, and cable expansion was measured at a number of locations. Expansion ranged from 0.508 to 1.22 millimeters per degree centigrade sheath rise. It was also interesting to note that the lowest temperature rise and millimeter expansion is at a lower elevation than the highest values.

Tests have been made by the Consolidated Edison in connection with factors affecting cable expansion. One of the factors studied was the affect of conductor lay in a three-conductor cable on longitudinal expansion. A curve was plotted of the results and is shown in Figure 16. The results may have been influenced somewhat by other factors affecting the cable expansion, but it is felt that conductor lay is important. I thoroughly agree with the authors of both papers that further extensive study and tests are necessary on this subject of cable movement and its relationship to sheath life.

Clement S. Schifreen: Appreciation of the importance of cable movement as a factor in determining cable performance is demonstrated by the amount of fine work done as well as in progress by several companies in the laboratory and field to correlate the various significant factors. This is reflected in the valuable operating and test data incorporated in the foregoing discussions. The material ranged from actual measurements under practical load conditions to purely theoretical computations, and in some cases outlined practical expedients in dealing with cracked sheaths in service.

Mr. Avila very kindly has brought us up to date regarding the experience in Boston with flexible brass bellows inserts and Thiokol tape in connection with cable replacements and repairs respectively, where manhole rehabilitation could not be justified. Their laboratory tests indicate adequate life to warrant their installation under appropriate circumstances, although their two or three years of service may be too short to evaluate whatever unknown factors might be involved.

Mr. Bodicky restricted his comments to computations of cable movement and sheath strain based on theoretical considerations. His graphic analysis of cable movement is helpful in visualizing the effects of the forces at work. Such a study, however, together with his mathematical analysis, although instructive, remains qualitative and does not portray the broad range of cable movements found under operating conditions. The crucial problem of establishing permissible loading limits requires more specific evaluation of the probability factors which defy purely mathematical analyses and can be secured only by field surveys. His very fine curves in Figure 5 would be more helpful if correlated with the ranges of movement found in actual field measurements on the particular size cable. It is unfortunate that Mr. Bodicky did not mention whether he tried to demonstrate the integrity of his formula for compression per unit length of cable by applying it to the Philadelphia test data set forth in Table III of my paper. I had found it necessary in my preliminary computations to discard a method of evaluation that assumed constant magnitude of strain over the curvature of the cable (somewhat similar to Mr. Bodicky's equation) in favor of the method outlined in appendix *A* of my paper in an effort to secure a good sheath life—per cent strain correlation as shown in my Figure 9. This is also corroborated by the strains actually measured (Figure 12 of my paper).

Mr. Clark is undoubtedly correct in stating that it is "impractical to determine where movement will occur." However, it is necessary to conduct field surveys in order to correlate cable movement with load and conductor-temperature fluctuations in a broad pattern of probabilities applicable to a large number of cable sections. There is no intention to predict what a particular section will do. These correlations, however, require hundreds of measurements and cannot be established by the limited number included in his tabulation. I do agree with Mr. Clark that the effect of per cent grade is masked by some of the other controlling variables which he listed.

Mr. Del Mar referred to an interesting instance of sheath breaks at wiped joints and duct mouths, where daily heating cycles were negligible, and suggested vibration as the probable cause. He then infers that vibration may have been a controlling factor in Philadelphia. We have had cases where vibration contributed to sheath breaks, for instance, in manholes at the ends of bridges, and at short distances below potheads on terminal poles. Furthermore, the analyses of sheath breaks on heavily loaded cables at times have been complicated by the fact that the conduit routes lay along busy city streets with heavy street-car and truck traffic. However, the worst concentration of failures caused by broken lead sheaths (analyzed in Table VI and Figure 11 of my paper) occurred along a route that did not involve truck or street-car traffic but only occasional freight trains on a spur line. Moreover, the sheath cracks were experienced exclusively on six heavily loaded cables in the conduit bank which also contained six relatively lightly loaded cables. After 18 years of operation there has not been a single cable failure or sheath break reported on the 72 lengths of the lightly loaded cables in the trouble area, whereas an excessive failure rate occurred on the equal number of heavily loaded cables in the same manholes.

Mr. Haines supplied corroborating evidence based on actual field experience that wider offsets result in better sheath life. The studies in Philadelphia also are in accord with the fact that the three-conductor 700,000-circular-mil 15-kv cable had a greater amount of sheath cracks than the 28-kv cables with a lower temperature range. The reversal for the 16–20-inch offsets, however, suggests a fortuitious combination of circumstances in a small number of cases.

Mr. Halperin indicated a gratifying consistency between the test results and experiences in Chicago and Philadelphia. The author gratefully acknowledges the personal assistance afforded by Mr. Halperin in connection with setting up the dummy-manhole test machine and the cable-movement survey in Philadelphia, but frankly is puzzled by the statement in Mr. Halperin's discussion regarding the "apparent assumption that the strain is independent of the diameter of the cable." The strain formula (equation 7, Appendix *A* in my paper) allows for cable size in the radius term, *r*. Furthermore, the derivation of the strain formula is not predicated on an assumption that "all bending occurs at points *A* and *B*," but follows the cantilever-beam analogy. This implies bending and consequent strains at all points along the beam, but with maximum strains at points *A* and *B*. The latter logically would determine sheath life and consequently warrant evaluation of strains at these points. As far as the characterization of Figure 6 is concerned, plotting of considerably more data are necessary to permit an accurate determination of the exact slopes of the curves involved, particularly in view of the great spread of the data. However, all quantitative analyses would be based on values taken directly from the curves.

Mr. Hatcher not only corroborated from his own tests and studies the conclusions stated in my paper, but also found that he could match the experience in New York involving broken sheaths on heavily loaded three-conductor 800,000-circular-mil cables with the results of computations based on the sheath-strain equation in Appendix *A* and Curve II of Figure 9. This should be very encouraging to all who are struggling with the difficult problem of determining optimum cable ratings. Mr. Hatcher's comment on the effect of snaking in the duct is being investigated in further tests in Philadelphia.

Mr. Hooven's description of the experience with single-conductor cable during tests by the Public Service Electric and Gas Company stands out in striking comparison with the single large peak of snaking by a three-conductor cable shown in Figure 19 of my paper. This phenomenon is being analyzed further with an improved technique in tests now under way.

Mr. Shanklin characteristically goes to the crux of the movement problem and discusses the reduction of maximum allowable copper temperatures on a graded scale for lower daily load factors. Practical considerations of rating cables in duct banks with multiplicity of circuits suggest cataloguing cables into several typical load-factor groups so that grading of allowable peak loads will fix the optimum daily movement for given manhole conditions. Maximum utilization of underground plant dictates such flexibility rather than a blanket rule of 100 per cent load-factor ratings. The author is in sympathy with Mr. Shanklin's present reluctance to change cable design in the interest of reducing cable movement. There is insufficient data on hand to prove the way, and misguided thinking may result in a cable inferior electrically without a commensurate improvement in the mechanical characteristics. Much experimental work remains to be done to clarify this point.

Doctor Wiseman inquired why a "reversed force" was needed to prevent the cable end from retracting into the duct. The answer is found in Figures 16 and 19. Although the cable originally was far from lying perfectly straight in the duct, its initial center line is shown as the horizontal zero axis to the left of Figure 19. As the conductor temperature rose and the force for total restraint was increased (Figure 16), the cable snaked considerably near the middle of the stretch. Although the cable shown in the right of Figure 19 cooled to one degree centigrade below the initial temperature, it retained a 0.4-inch maximum vertical deflection, despite the application of about 350 pounds to maintain the cable length at its original value. Apparently, the cable, once forced to "snake," required a greater force to reduce the snaking deflection than could be exerted by the frictional force alone of the cable along the duct. Consequently, the cable would have retracted into the duct, were it not for the external force applied in "reverse."

C. T. Nicholson: The effect of the ratio of cable diameter to duct diameter on longitudinal cable movement may be gleaned from the interesting tests which Mr. Hatcher has made. In the first test where the cable diameter was 83 per cent of the duct diameter, longitudinal motion was over two millimeters per degree centigrade increase in sheath temperature. In the second test where the cable was less than 75 per cent of the duct diameter, cable motion was around one millimeter per degree centigrade.

It is interesting to note that the diameters involved in Mr. Hatcher's second test closely parallel the diameters on the Buffalo tests, and that the realized expansion is almost identical. This is of particular interest since the New York cables were single conductor whereas the Buffalo cables were three conductor.

Mr. McCabe has called attention to the fact that tests conducted in Detroit show that an increase in length of load cycle, rather than an increase in final temperature level, is effective in increasing longitudinal expansion. In all probability the variation in the coefficient of realized longitudinal expansion is a function of both length of load cycle and temperature level. As indicated in the report of the Buffalo investigation, expansion may be related to the expansion of the lead sheath and the copper conductors. If a load is applied to a cable for such a length of time that the rise of sheath temperature is approximately one half of the rise of the conductors, the free elongation of the sheath and conductors will be identical. Under this condition the cable will suffer no internal longitudinal forces and consequently will tend to snake in the duct. The coefficient of realized expansion under this condition is apparently in the order of 0.01 inch per 100 feet per degree centigrade of sheath-temperature increase.

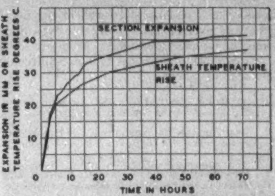

Figure 17. Sheath-temperature increase and section elongation for 72-hour 325-ampere load cycle on section between manholes 317 and 347 (curve C)

A combination of loading and load cycle, such that the sheath temperature rise exceeds 50 per cent of the rise of the conductors, will produce a sheath expansion in excess of the conductor expansion, and the internal stress so created may account for an increase in the coefficient to a maximum of 0.015 inch per 100 feet per degree centigrade.

Of course, the coefficient will vary from other causes, such as the amount of internal stress and snaking present at the start of the load cycle, the total capacity for snaking, and possibly the rate of heating and the thermal constants of the cable and duct bank. Figure 17 shows graphically the measured expansion and sheath temperatures taken for the section between manholes 317 and 347 for the 72-hour 325-ampere test reported in the paper. Computing the coefficient at several stages during the heating cycle brings out its variable nature. At the end of two hours of heating it is 0.0112; at 16 hours it is 0.014; it reaches a maximum of 0.0144 at the end of 40 hours; and it falls to 0.0132 at the end of 72 hours. Figure 18 shows this variation graphically.

In reply to Mr. McCabe's questions about the possibility of error in sheath-temperature and ambient-temperature measurements produced by the presence of the bellows mechanism and the steel pipe, it should be pointed out that some preliminary investigation, as well as experience over a number of years, has indicated that the accuracy of measurement on the average is superior to other methods, with the possible exception of very elaborate arrangements employing thermocouples soldered to the cable sheath. The error indicated by a laboratory study of soldered couples and the pressure bellows couple was less than one degree centigrade. Thermal variations in the duct section are of more consequence than difficulties with the bellows couple.

The buried steel-pipe system used for ambient-temperature measurement was carefully checked in the field for errors of the type suggested by Mr. McCabe and was found to be sufficiently reliable.

On the subject of arcproofing, the arcproofing was removed from the cables on test to permit close observation of the sheath reaction to the expansion cycles. The arcproofing, if it were allowed to remain, would not have changed the results in

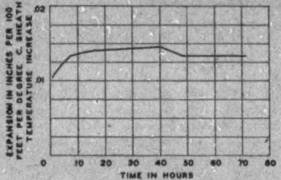

Figure 18. Variation in realized coefficient of longitudinal expansion for load cycle of Figure 17

any way, because it consists simply of a felted asbestos tape impregnated with water glass and consequently has no rigidity.

It is interesting to note in Mr. Hatcher's remarks that he has observed a correlation between duct profile and thermal profile. The fact that this tendency existed for all of the sections extensively tested in Buffalo suggests that thermal studies in general can be made at the higher end of each section. The sections between manholes 205–247 and 347–379 from the data given in Figure 7 and Figure 1 of the paper do not display this characteristic as well as the other sections on the tests. This may be a function of fact that the profile of Figure 1 does not represent actual field conditions as to the exact position of the ducts throughout the section.

There is undoubtedly some error in the sheath-temperature measurements, which were taken as close as six or seven feet from the duct entrance. However, the error, if any, is made consistently. W. B. Elmer in a report to the transmission and distribution committee of the Edison Electric Institute in 1937 indicated that temperatures taken at this location were substantially correct.

I. A. Rohrig's discussion on the movement of aerial cable is very interesting and closely parallels the reaction of cable suspended in rigid conduit on long bridges. In fact the ambient-temperature limits for bridge installations may exceed those of any other type of installation, and this has been known to produce sheath failure without the assistance of load-current heating. When a cable structure is heated entirely from external sources over long lengths and without the benefit of expansion loops, the excessive expansion of the sheath over the core readily produces circumferential sheath buckles and ruptures, particularly in the vicinity of joint wipes.

Added investigation on the subject of expansion is needed urgently, if the cable now in operators' hands is to be put to the service of which it is inherently capable. Means of reducing the rate of damage, whether it be the result of sheath buckling from bending or longitudinal stress or a combination of these two, is the most pressing need. In his recent paper Mr. Schifreen has given us an excellent means of determining the sheath life which may be expected from installations as they are currently made. Means must be found, however, to improve sheath performance, preferably by modification of installation practices, so that a great deal more service can be expected.

Gas-Filled-Cable Research and Experience (44-133)

Operation of Low-Pressure Gas-Filled Cable (44-134)

Discussion and authors' closures of paper 44-133 by G. B. Shanklin, and paper 44-134 by C. T. Hatcher, presented at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944, and published in AIEE TRANSACTIONS, 1944, August and November sections, pages 549–53 and 788–92.

R. W. Atkinson (General Cable Corporation, Bayonne, N. J.): In his paper Mr. Shanklin contributes important data. However, some of his conclusions appear to be based not on these or other available data. In part, this discrepancy seems to be due to his placing perhaps an unwarranted weight on the "self-healing property." He discusses this subject in a very interesting manner but he, himself, gives reason to discount much of its practical importance. For example, Mr. Shanklin states that at the higher pressures "this working-voltage stress is well below the ionization-voltage stress" and cites specifically cables designed for 80 and 40 pounds per square inch to confirm this point. Inasmuch as he also contends that it is ionization which produces this self-healing, it is difficult to see how he can expect any self-healing to occur in service at these pressures. He even indicates that what he says of higher pressures is true, though to a lesser degree, in cables intended to operate at a pressure as low as 12 pounds. He expects, however, that future reductions of insulation thickness will make operating stresses higher with respect to ionization stresses. This apparently may make the self-healing feature of some significance at very low gas pressure, but there is no reason to suppose that any such changes can bring stresses in high-gas-pressure cable to the point where the self-healing feature can be of any practical significance.

Another cause for what I believe to be erroneous deductions is that he has formulated these on the basis of tests or experience at low-gas pressure and has stated these as though they apply without qualification as to degree of gas pressure.

It is clear that the paper is intended to apply to high- and medium-gas-pressure cables as well as to low gas pressure, and the author cites successful operating experience of medium- and high-gas-pressure cables. It should be noted, however, that the only high-gas-pressure cables on which there are service data depart sharply in many ways from the materials and construction which in the author's conclusion he specifies as necessary or desirable.

It is well to study the author's specific conclusions in respect to gas pressure. Except for measurement of initial ionization, he has shown no new data pertaining to high pressure. Specifically, he has no endurance tests on other than low gas pressure. The first paper shows that four samples were given load-cycle tests at high pressure, but these tests apparently were not encouraging and perhaps helped lead to the discouraging view the author took at that time concerning the use of high gas pressure. Hence these previous tests hardly can be the basis of new conclusions as to what can be expected from high gas pressure.

The author's seven individual conclusions may be discussed in order:

I agree with his statement that gas-pressure control gives close control of performance characteristics.

His second conclusion is to the effect that in general average stress rather than maximum stress determines performance of mass-impregnated, gas-filled cable and that it is not necessary specially to strengthen the inner zone of insulation in the high-stress field near the conductor. He properly confines this conclusion to mass-impregnated cable as he should do with the remaining conclusions. The tests and theory indicating "self-healing" lend some support to this conclusion but are entirely inadequate, without suitable comparative tests, to warrant so unqualified a statement. It is to be noted that all of the tests he reports, except on certain sector conductors, are on cables having the comparatively low ratio of about 1.3 of maximum to average stress. There is no basis to indicate that the conclusions would hold for high values of this ratio obtained with thicker insulation walls or smaller conductors. On sector cables there is a good basis to expect higher specific strength at the corners and the data on these are not directly applicable to other types.

He states: "Strand shielding of mass-impregnated gas-filled cable has shown no marked benefit." No experimental data leading to this conclusion are indicated. Also it should be noted that theory indicates that strand shielding has its most fertile field where thin tapes are used; no indication is shown that this has been considered.

He concludes that "Extra thin paper tape in the high-stress field has shown no marked benefit in mass-impregnated, gas-filled cable...." In the 1939 paper,[1] he stated that "the thinnest paper tape allowed by practical limitations (5 to 6 mils) should be used." He gives no hint of having found means of avoiding his former "practical limitations" and of having tested "extra thin tapes." Thus his present conclusion is not only at variance with the technical implications of his former conclusions but appears to be without an experimental basis.

His fifth conclusion reads, "Extra-high-viscosity impregnating compound lacks self-healing properties and should not be used in gas-filled cable." In the test he now reports with high-viscosity oil, it should be noted that the apparent instability is due to the increase of low-voltage power factor—not to the difference between high- and low-voltage power factors, that is not to ionization. Ionization continued to decrease throughout the test. If failure was imminent from instability, it was not indicated clearly to be from ionization. The increase of low-voltage power factor may well be of the sort that has occurred with many cables from the effect of heat alone. No basis is indicated why, even with the type and construction of cable tested, the result is not due to deficiency of the saturant with regard to time-temperature stability of power factor, instead of to its viscosity. In the 1939 paper, a report is made of a test with another type of high-viscosity saturant with somewhat similar result, and it was recognized then that the results probably were caused by the grade of material used, but a similar explanation seems now to have been overlooked. Contrary to Mr. Shanklin's experience with high-viscosity oil, we have used other types of high-viscosity saturant and have found them remarkably stable.

There is no occasion to comment here concerning the conclusion which concerns very fluid oil.

The author states in effect that optimum results are obtained with an intermediate impregnating compound of sufficiently high viscosity. This conclusion falls, along with his fifth on which it depends. In short, Mr. Shanklin's unfavorable result with high viscosity oil is simply a result of the character (clearly unsatisfactory) of the oil he used for this test. He mistakenly has attributed this result to the viscosity of the oil instead of to its other characteristics.

Reference

1. Low-Gas-Pressure Cable, G. B. Shanklin. AIEE Transactions, volume 58, 1939, July section, pages 307–15.

Robert J. Wiseman (The Okonite Company, Passaic, N. J.): Mr. Shanklin's paper may be considered as a progress report on previous papers presented by him on the subject of gas-filled types of paper cables and as such contains some very interesting data.

When we read this paper, we must keep in mind that Mr. Shanklin is describing and discussing a particular kind of a gas-filled cable, namely, one that is first impregnated as a solid-type cable by what is known as mass impregnation. The insulation (paper) and spaces between layers are thoroughly filled with oil and then deliberately drained of as much oil as possible with the intention of replacing the drained oil with nitrogen gas. Here we have a combination of oil and gas through the insulation, and Mr. Shanklin shows the power-factor-ionization characteristics of such a type of cable. A second type is the so-called Beaver type also known in the United States as the SMD cable in which preimpregnated paper tapes are wrapped on the conductors, and, as there is no excess oil to be drained, the spaces between tapes are already prepared to receive gas. This type of cable will have its particular power-factor-ionization characteristics which already have been reported in papers at the June 1942 AIEE convention in Chicago by representatives of the Detroit Edison Company and the General Cable Corporation. There is a third type which has been developed by the Callender's Cable and Construction Company of England and which the Okonite-Callender Cable Company is introducing in this country which takes the mass-impregnated cable and impresses on the cable a gas pressure. This cable also will have its particular power-factor-ionization characteristics. A 1942 article gives information on this cable.[1] Since then, we have tested single-conductor 132-kv cable and obtained the values of average voltage stress given in Table I at which ionization started for various pressures. This is comparable to Figure 2 in Mr. Shanklin's paper.

I feel that Mr. Shanklin has overemphasized the influence of the viscosity of the impregnating compound on the life stability of a gas-filled cable. Perhaps it is peculiar to his type of gas-filled cable. I am inclined to believe that, when he used an oil having a 1,000 second viscosity at 100 degrees centigrade, it was so heavy that it originally did not permit proper impregnation of the paper tapes and the space between the tapes; so that, when he drained the cable, he had an improperly impregnated cable and a low-quality cable with which to start. On the other hand, when he tried a very thin oil like number 5314 oil, it, being so fluid, easily drained out of the paper as well as from between the layers of paper and gaps, and he had practically an oil-free insulation which would, in time, show instability, because it was operating at too high stresses for that kind of insulation. We agree with him that impregnating compounds in the region of 100 to 200 seconds Saybolt viscosity are the best for gas-filled types of cable for his design and also for the Callender's design.

As the three types of gas-filled cables have different degrees of oil saturation, we find it necessary to use different insulation thicknesses. For the present, on the Callender design, we here are using the same thicknesses as for oil-filled cables. The company is furnishing to the Central Electricity Board in England a three-conductor 132-kv cable of following construction: conductor size is 0.4 square inch (518,000 circular mil); thickness of insulation is 0.500 inch, including metallized paper which is the insulation shielding; in one of the interstices of the assembled conductors is a gas channel pipe of alloyed lead $^5/_{32}$-inch outside diameter, $^1/_{16}$ inch thick; in another of the interstices is a two-conductor, lead-sheathed pilot cable 0.484-inch outside diameter; copper-woven fabric tape binder; 0.190 inch lead sheath with an inside diameter 0.100 inch larger than core diameter of cable; one copper-woven fabric tape; one layer of 56 steel strips $^1/_4$ inch wide, 0.020 inch thick; one layer fabric tape; one layer of steel tape 2 inches wide, 0.020 inch thick with $^1/_8$ inch gap; one layer fabric tape; one layer of steel tape, 2 inches wide, 0.020 inch thick, $^1/_8$ inch gap. The over-all diameter will be 5.09 inches. This cable will operate under a 200 pounds per square inch pressure.

Table I

Pressure, Pounds Per Square Inch	Average Voltage Stress at Which Ionization Started, Volts Per Mil
0	70
25	110
50	150
75	180
100	200
125	230
150	Over 260
175	Over 260
200	Over 260

Reference

1. New High-Gas-Pressure Cable Is Developed in England, R. J. Wiseman. *Electric Light and Power*, July 1942.

T. F. Peterson (American Steel and Wire Company, Cleveland, Ohio): In offering a general discussion on Mr. Shanklin's paper, I am torn between two strong urges—to expound Emerson's essay on "Compensation," or to tell the story of Oscar who found mixed satisfaction and disappointment in the loss of one wife and the subsequent acquisition of another with her many children, and her huge house and accompanying property.

It seems that the production of gas-filled cable necessitates delicate balance and compensation of many diverse variables and characteristics. The cable is first impregnated thoroughly to provide what would normally be considered very good insulation, and then it is quite thoroughly drained, with resulting bad properties. Then it is filled with gas which under pressure suppresses ionization, and this of course is desirable. However, there appears to be intentional ionization to produce good dielectric wax. But the incidental consumption of compound necessitates replacement. Viscous material, though possessing nonmigrating (good) qualities, is not self-healing, whereas thin compounds possessing remarkable self-healing properties are bad because they "drain too readily for safe use in gas-filled cable." The final structure obtained by judicious balance of these extremes, seems to be one that develops in operation an unusually high percentage of sheath failures, which, of course, would be very serious in normal cable, and in the case of gas-filled cable is compensated by the sheath supervisory system, which provides alarms prior to insulation failure at sheath breaks.

It would seem that by contrast with existing oil-filled or solid cable, so many critical boundary conditions have been set up which limit or define the region in which cable with satisfactory test qualities appears, that it would be difficult to realize uniformity of cable quality—foot by foot and day by day. Even when the optimum conditions are reached, we have what may be called an ionization-free cable, but wherein have we either tests or operating results to support the suitability of this cable from a mechanical and thermal point of view. The claim is made that "the cable is the only known type that offers a direct way of predetermining safe operating voltage stress and insulation thickness by means of a simple short laboratory test." This, of course, is only true insofar as ionization voltage is concerned and exists only because other types of cables do not show ionization voltages near the chosen operating range.

I doubt if anyone will question any of the findings insofar as curves in Figures 1 to 5 are concerned. It would appear, however, that some of the data have been extrapolated unreasonably far into what normally would be operating life, and certainly tremendous importance is placed on a single point, approximately determined, to form the middle position of the curve of Figure 7. Cable of Figure 6c shows maximum ultimate as well as maximum percentage increase in ionization starting voltage, and shows maximum life test—yet is ruled out. Both 6c and 6b show progressive improvement in ionization up to the point of failure. What causes failure at point of maximum ionization starting voltage?

Undoubtedly mechanical and thermal characteristics of the cable have been given consideration. It would be interesting to have the presentation of such of these characteristics as may have been determined. Is there any possibility that relatively dry paper gas-filled valleys and perhaps loose sheath caused by slight stretching from the core may result in unusually high thermal resistance from copper to sheath and that this in turn may contribute to high differential expansion of copper as compared with lead, resulting in sheath buckling and breakage? As clearly indicated in other papers presented this afternoon, these mechanical characteristics are many times more important in determining cable-failure rate than the inherent electrical properties of the insulation. It would be helpful, I think, if the picture could be rounded out with some discussion of these properties.

K. S. Wyatt (Phelps Dodge Copper Products Corporation, Yonkers, N. Y.): Has Mr. Shanklin drawn a logical conclusion from the results of his experiments relating viscosity of compound to life of gas-filled cables? In one experiment he takes a standard cable oil of *medium* viscosity and finds it results in a cable having "self-healing" properties and hence of long life. This medium viscosity oil is chemically unsaturated. Then in a second experiment he takes a standard cable oil of *low* viscosity and finds it gives a cable with fairly good self-healing properties and hence of fairly long life. This oil is also chemically unsaturated. Finally, he takes a special hydrogenated oil of *high* viscosity and finds it results in cable having no self-healing properties and hence of short life. This oil, being hydrogenated, would be expected to be chemically saturated. From these results he concludes that a high-viscosity oil results in a gas-filled cable having short life.

This I believe to be an erroneous conclusion. Would it not be more logical to say that these experiments demonstrate that a chemically saturated oil results in severe ionization in such cable and, hence, in short life? Supporting this is more than 20 years of experience in both the practical and scientific work of the cable industry. Chemically saturated oils give off great quantities of gas under electric discharge and magnify ionization troubles in cable. One kind of chemically saturated oil is petrolatum, and we all know the ionization and life characteristics of cable made with that. In England important cable lines impregnated with oils nearly saturated chemically have had to be drained and the oil replaced with a chemically unsaturated oil of approximately the same viscosity, because the saturated oil caused serious gassing and ionization troubles. Moreover, the goal toward which we all have been looking for some time has been a chemically unsaturated cable oil having good oxidation stability.

It is true, of course, that, if this reasoning is correct, Mr. Shanklin's explanation of why gas-filled cable works would need considerable rehabilitation, and this is unfortunate. However, we may be comforted in that there are other explanations of why gas-filled cable operates satisfactorily.

Although Mr. Hatcher's paper leaves little doubt that gas-filled cable is a practical solution at low and medium voltages, I feel that a note of warning should be sounded concerning this type of cable at extra-high voltages. I wish to call attention to the voltage-time-to-breakdown curves of Figure 1 and their significance. Oil-filled, compression, and oilostatic cables reach an asymptote after four to six hours, and the long-time value is high, that is, about 1,000 volts per mil maximum stress.[1] The life of solid cable falls off with time for 100 hours or more and reaches an asymptote at the low value of 438 volts per mil (Enfield Cable Works, London, England). Also, see D. M. Robinson's book.[2] The difference between the shape of the curve for oil-filled types of cable and that for solid cable usually is explained by saying there is no ionization in the oil-filled types of cable, whereas ionization in the solid cable progressively deteriorates it. Now we come to the curve for 120-kv gas-filled cable (taken from Figure 13 of AIEE paper[3]) which has roughly the same shape as that for solid cable, reaching an asymptote after about 100 hours, and at the low value of 420 volts per mil (compare this with the oil-filled types at 1,000 volts per mil). Even to achieve this low value, conductor shielding had to be employed, and it is believed dense paper also. It seems that here, as with solid cable, ionization causes a progressive deterioration.

It is true that breakdown curves of this type represent accelerated tests which cannot be extrapolated to normal operation. However, they do furnish us with a clue as to possible types of deterioration in operating cables. To my mind the lesson is that we should watch very carefully for signs of deterioration caused by ionization in extra-high-voltage gas-filled cable over a period of years, as the deterioration, if any, is likely to be cumulative.

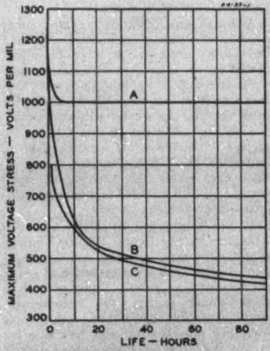

Figure 1. Voltage versus time-to-breakdown curves of several types of cable

A—Oil-filled cable types: oil-filled, compression, and Oilostatic

B—Solid-type cable

C—120-kv gas-filled cable at 225 pounds per square inch, nitrogen

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1. THE PRESSURE CABLE, Bowden-Main. *Electrical Power Engineer*, November–December 1933.

2. DIELECTRIC PHENOMENA IN HIGH-VOLTAGE CABLES (book), D. M. Robinson. Chapman and Hall, London, England, 1936. Page 10.

3. 120-KV HIGH-PRESSURE GAS-FILLED CABLE, I. T. Faucett, L. I. Komives, H. W. Collins, R. W. Atkinson. AIEE TRANSACTIONS, volume 61, 1942, September section, pages 658–65.

Victor Siegfried (Worcester Polytechnic Institute, Worcester, Mass.): Mr. Shanklin's paper on the experience of gas-filled cables is quite gratifying, as it indicates continued progress in making better use of insulating materials through control of the gaseous portions of the insulating medium. The use of pressure long has been attractive, since the early work of Paschen, and yet we have not begun to realize in a practical way the full value of additional strength secured in this way. It is almost superfluous to suggest that even higher pressures than those described offer additional advantages to offset the increasing difficulties of containing the higher pressures mechanically.

Where Mr. Shanklin appears to be disappointed in the increase in voltage for initial ionization at higher pressures, where in Figure 3 it departs from theoretical values by Paschen's law, he should take courage from other work done at both high and low pressures showing marked departures from this law. At less than atmospheric pressure, such as encountered at high altitudes, the voltage for breakdown has not decreased as fast as the pressure or air density—a decided benefit. At higher pressures the same departure occurs and must be accepted, even though it is not advantageous. In some work done by the discusser both at Stanford University and Worcester Polytechnic Institute, the so-called Paschen's law relationships have been found to relate to air density at a $^2/_3$ power rather than directly. That is, ionization voltage is varied as (relative air density)$^{2/3}$. Although this is a somewhat empirical figure, it indicates the direction that may be expected in such comparisons.

In spite of this effect, there is still much to be realized in progressing to true high-pressure cable insulation. Marked changes may be necessary in basic cable structure and terminating equipment, but it seems appropriate to keep in mind the possibility of working up toward the plateau pressures from 200 to 250 pounds per square inch.

L. F. Hickernell (Anaconda Wire and Cable Company, Hastings-on-Hudson, N. Y.): After all these years of worry about our old enemy "X" wax, it is quite a relief to learn that all the time he was a friend in disguise whose "X" mask is now removed to reveal his true character—a self-healer!

Data are presented by Mr. Shanklin on three oils with a range of viscosities:

1. Hydrogenated—1,000 Saybolt universal seconds at 100 degrees centigrade.

2. Standard 5317—100 Saybolt universal seconds at 100 degrees centigrade.

3. Thin 5314—37 Saybolt universal seconds at 100 degrees centigrade.

Hydrogenated oil is of a different base than the other two oils. Hence, there are two variables (oil base and viscosity) in the experimental data which do not support conclusions drawn from a viscosity analysis only.

The last two oils, numbers 5317 and 5314, are of a type which *does not* form wax readily. If the presence of wax is desirable from a "self-healing" standpoint, it would seem logical that the next step would be to replace the standard number 5317 with a type of oil which *does* produce wax readily.

Wm. A. Del Mar (Phelps Dodge Copper Products Corporation, Yonkers, N. Y.): The self-extinction of ionization is a fact of basic importance in the design and operation of gas-filled cables, and its proper understanding will influence, in no small measure, the future of this important type of cable.

For this reason I want to bring under scrutiny the very ingenious and plausible theory which Mr. Shanklin advances in his paper, as I do not find that the theory squares with all the facts as I know them.

According to Mr. Shanklin's theory the migration of oil under the influence of stress differentials and capillarity should take place at voltages just below the ionization point about as readily as at voltages just above the ionization point. In my experience that is not the case under load-cycle conditions. Even with load-cycle tests of gas-filled cable at voltages slightly above the ionization point we found plenty of free oil at the outer layers, the amount decreasing with depth. The laboratory report for 5317 oil says: "Tapes very dry at conductor"; and in the case of the more viscous oil, "Tapes rather dry near the conductor."

Although an accurate analysis of the forces is very involved, a cursory study indicates that the electric forces at play are very small compared to those produced by thermal expansion and that whatever redistribution of compound may occur in load cycles is largely the result of the latter. As the paper has higher specific inductive capacity than the oil, it is the paper, rather than the oil, that tends to move toward the conductor and thereby prevents the return of part of the oil expanded outwardly in load cycles.

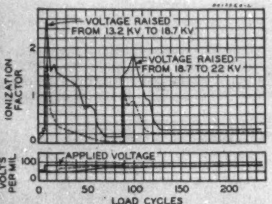

Figure 2. Ionization of low-pressure gas-filled cable at room temperature

Pressure is ten pounds per square inch

Ionization factor is the difference between power factor at 20 volts per mil and at test voltage

—— Number 5317 oil

– – – Rosin-blended oil

The suppression of ionization is, in my opinion, the result of ionization and cannot take place at the rate observed unless ionization is present. It occurs equally with voltage alone at room temperature or with voltage and current in load-cycle tests.

Tests with voltage alone made by J. H. Palmer in 1934 showed that a drained cable which had an initial ionization stress of 52 volts per mil (maximum stress), after 109 days at 60 volts per mil, had its ionization stress raised to 65 volts per mil. This cable differed from the type of cable under discussion in containing air at atmospheric pressure instead of nitrogen at higher pressure. In this cable the inner layers of tape at the conclusion of the tests showed more oil than the outer. Perhaps, when heating effects are absent, the electric force is sufficient to operate. Indeed Mr. Palmer commented in his report, "The only explanation I can offer for this is that it was caused by redistribution of the oil, and this explanation is not very satisfactory."

Another experimental fact which controverts the oil migration theory is that cables made in our laboratory with high-viscosity oil showed less wax and markedly more self-extinction than those made with 5317 oil, after the load-cycle tests. This difference in favor of the high-viscosity oil persisted through about six months of daily load cycles, after which their ionization voltage became and remained equal. That Mr. Shanklin obtained the opposite results with his oil was not a matter of viscosity but of the peculiar chemical characteristics of his very unusual type of viscous oil.

Our test results are shown in Figure 2, at the bottom of which the applied voltage stresses and ionization stresses are shown, while the resultant ionization factors are shown above. Data are given both for cable made with 5317 oil and for a more viscous rosin-blended oil. The more rapid and more complete suppression of ionization with the latter oil, is evident.

Although wax formation may necessarily precede self-extinction of ionization, it is not the primary cause, that is, it is not a matter of choking the interstices with wax, because we have found in our load-cycle tests that wax formation was considerably less in the cable showing the greater self-extinction.

I do not feel confident that I know the correct explanation of self-extinction of ionization, but I rather suspect that it results from either the lowering of stress across the inner gas-filled tape gaps or an increase of gas pressure therein.

It was shown by Hirshfeld and collaborators in The Detroit Edison Company that the formation of cable wax is accompanied either by a great evolution of gas or the formation of water. Either or both of these would explain the phenomenon. Another possible explanation is that cable wax, like polyethylene and several other organic insulating materials, forms a slightly conducting surface layer when subjected to electrical discharges in gas. I have no evidence that this occurs and mention it only as a possibility.

Self-extinction of ionization remains an open field for investigation.

I endorse all that Mr. Shanklin has had to say about the success of low-pressure gas-filled cable and the permanent place it has won for itself both for technical and economic reasons.

Joseph Sticher (The Detroit Edison Company, Detroit, Mich.): The detailed information presented by Mr. Shanklin concerning the self-healing effect that wax formation in cables produces by way of extinguishing ionization in voids is of particular interest. Wax formation occurring in cables has been looked upon for a number of years by some of us[1] in Detroit as a sort of natural self-defense of the cable against the ravages of ionization occurring within the cable. This opinion is expressed also in a paper, "The Effect of Corona Discharge on Cable Insulation," by Joseph Sticher, D. E. F. Thomas, C. D. Robb, and F. M. Hill, presented at the conference on electrical insulation of the division of engineering and industrial research of the National Research Council in November 1938 in Pittsburgh, Pa. In his paper Mr. Shanklin states that "the puzzling thing has been where all of the surplus compound came from that formed this wax, which completely or partially filled up the void spaces" and that "if the impregnating compound is of sufficiently low viscosity to be mobile it will be drawn into the high-stress field both by capillary action and by the electric-stress field itself." I am just a bit skeptical of this explanation and wonder whether Mr. Shanklin has made calculations of the electric forces involved in this mechanism. A rough calculation indicates that the electric force involved is negligible. As to the capillary action, it would seem that, unless there is an amount of excess oil outside the insulation that is available for the insulation and of a larger amount than the combined void volume inside the insulation, this mechanism might be detrimental rather than helpful, inasmuch as the larger voids might serve as reservoirs for the smaller ones and become entirely depleted of oil. In a solid-type cable there probably is such excess oil, but whether this is true for a low-pressure gas-filled cable that is purposely drained of excess oil is problematical. In any case it would appear that neither electric stress nor capillary action as it exists in cables could be a great factor in oil movement, otherwise one would expect to find that cables that have been in service for some time would be nicely impregnated throughout as long as excess oil exists outside of the insulation. This is known not to be the case, as evidenced by dissection of cables.

In our early attempts to explain the self-healing effect occurring in solid-type cables the following reasoning was employed: Since wax is a result of ionization in a void, it will form only in those voids where ionization occurs, and these voids are the only ones with which we are concerned. If the impregnating compound is of such a nature that wax is readily formed, then a protective layer of wax will be formed over the void surface in a short time, and this layer will serve to decrease the volume of the void. During a subsequent loading cycle impregnating compound will be forced into the void as a result of the thermal expansion of the compound and accompanying increase in pressure surrounding the void, and a certain amount of this compound will be

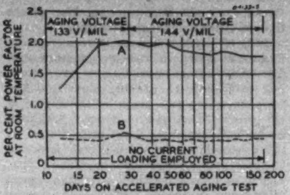

Figure 3. Effect of accelerated aging without load cycles

Three-conductor 350,000-circular-mil shielded solid-type cable

Standard compound impregnated paper, 20/64 inch

A—111 volts per mil

B—18.5 volts per mil

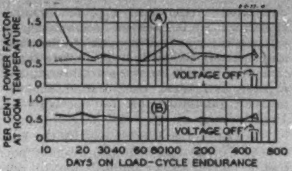

Figure 4. Load-cycle endurance test on two samples of solid-type cable

A—Evacuated sample; evacuated to ten inches mercury at room temperature

B—Nitrogen-filled sample; filled to ten pounds per square inch with nitrogen at room temperature

—— 77 volts per mil

– – – 18.5 volts per mil

Load cycle—six hours on; 18 hours off (65 degrees centigrade conductor temperature)

retained on the surfaces of the void, when the load is taken off the cable. This fresh layer of compound will be bombarded again, and additional polymerization (or condensation) will occur with consequent further reduction of the void by wax, since the polymerized material, being semisolid, is no longer free to escape from the void. In this manner loading cycles, ionization, and resulting wax formation will continue to reduce the void size, until further ionization is no longer possible, that is, the "sore spot" is healed. If the waxing ability of the compound is low, however, the insulation might fail before sufficient wax can be formed adequately to reduce the size of the affected void.

In this connection, I wonder whether in Mr. Shanklin's cables self-healing would have been observed with the thin-oil and with the standard-compound impregnants, if load cycles had not been applied, that is, if oil movement into the voids does depend on capillary action and on electric stress, then self-healing should be observed also when load cycles are not used. Figure 3 of this discussion shows the results of an accelerated aging test conducted at the research laboratory of The Detroit Edison Company in which a three-conductor 350,000-circular-mil, shielded solid-type cable with paper insulation of 20/64-inch thickness impregnated with standard compound was subjected to an average stress of 144 volts per mil for 144 days without load cycles. The impregnating compound was somewhat more viscous than the standard compound used by the author, but its viscosity was well within the range of that of compounds used for solid-type cable. From the differences in power factors measured at 111 volts per mil and at 18.5 volts per mil it was concluded that self-healing was practically nonexistent. The type of mechanism of self-healing is of practical importance, when cable has to remain energized at extremely light loads for periods of long duration (depression, strikes, shifting of load areas, and so forth).

Figure 3 brings up the question of performance of low-gas-pressure cable as compared with that of solid-type cable. Comparison of Figure 3 of this discussion with Figure 4 of the paper shows that the endurance characteristics of the solid-type cable under high-potential stress compare favorably with those of the low-gas-pressure cable, particularly when it is taken into account that the solid-type cable did not have the benefit of compound redistribution provided by load cycles.

An investigation that was started at The Detroit Edison research laboratory approximately two years ago and is still in progress adds some information concerning the effect of gas pressure in cables. The test was undertaken to determine the feasibility of operating 24-kv solid-type cable, joints, and duct splices at 41.6 kv, as judged by the comparative test life of two experimental cable setups. It was anticipated that it would be necessary to provide a positive gas pressure in the cable in order to make it operate safely at the increased voltage. For this reason the cable, joint, and duct splice of one setup were filled with nitrogen to a gage pressure of 10 pounds per square inch. The other setup was to simulate cable conditions as they exist in service operation of solid-type cable and was evacuated to 10 inches mercury. In both setups a potential of 41.6 kv (24 kv between conductor and ground, or 77 volts per mil) was applied continuously, and daily load cycles to reach 65 degrees centigrade conductor temperature were used. It was intended to determine the time to failure and to follow the progress of any deterioration by means of power-factor measurements. The two pieces of cable were cut from a length of 350,000 circular mils three-conductor round, 20/64-inch paper-insulated shielded lead-covered cable that had been manufactured in 1929 and had been in service for $11^1/_2$ years. Figure 4 of this discussion shows the load-cycle endurance of both specimens up to the present, as indicated by power-factor values measured on one conductor at 77 volts per mil, 18.5 volts per mil, and at room temperature. At the end of 550 cycles there is little difference in the power factors of the two samples, and the ionization in both samples is zero. The variations of ionization with time for the evacuated cable probably are caused by disarrangements of compound and subsequent self-healing. It is interesting to note from the figure that both specimens, the one at ten pounds per square inch nitrogen and the other at ten inches mercury, stood up well under the average stress of 77 volts per mil, particularly in view of the statement in Mr. Hatcher's paper that the average stress for the gas-filled cables operating on the Consolidated Edison Company system ranges between 39 volts per mil as a minimum and 68 volts per mil as a maximum.

Reference

1. **Discussion by Joseph Sticher of Low-Gas-Pressure Cable, AIEE Transactions, volume 58, 1939, page 317.**

E. Richman (General Cable Corporation, New York, N. Y.): Once more Mr. Shanklin gives us new and interesting data on a type of cable which he rightly states no longer can be considered experimental. Regarding specifically the high-voltage high-pressure gas-filled cable, it should be borne in mind that the only high-pressure gas-filled cable installation in the United States, namely the 120-kv SMD installation in Detroit, differs in design from the type discussed by the author. Likewise the Beaver-type cable installed in England, which now totals over 100 conductor miles, including 25 miles of 132-kv cable, differs from Mr. Shanklin's design in a similar manner. These cables are not mass-impregnated but are insulated with preimpregnated paper tapes in order to eliminate any possibility of physical or electrical trouble from compound drainage even on vertical runs of any height. In the Detroit cable a very high-viscosity saturant (3,000 seconds Saybolt Universal at 100 degrees centigrade) was used, whereas the English cable contained either a high-viscosity saturant, or very special means were used to remove all excess surface compound from the individual tapes. The SMD cable and the Beaver cable used conductor shielding and tapes of graded thickness beginning with very thin tapes of $2^1/_2$ mils and less in the high-stress region next to the conductor.

Mr. Shanklin intimates that the design he has discussed, which has proved itself technically and economically sound for low-gas-pressure operation, therefore should be used for high-pressure operation. It has been found desirable to use average stresses of 65 to 75 volts per mil for this low-gas-pressure cable, whereas economic considerations and practical dimensions dictate going to maximum stresses of 200 volts per mil or more for the high-pressure type. Technical considerations or design details certainly will carry different weight for these widely different stresses.

The author has emphasized in this paper and in previous papers that the beauty of gas-filled cable lies in the fact that the critical voltage stress can be determined far more accurately and therefore can be more safely approached than in the case of other types and that this is so because the size of the voids and the pressure in the voids are under control. This is very true, but full advantage is taken of the control over void size only when strand shielding and thin tapes are used. The strand shielding eliminates the stress in the voids caused by the conductor strands, and thin tapes reduce the thickness of the tape butt spaces, thus taking advantage of the increase in specific dielectric strength of gas in very thin films. An additional fact, undoubtedly of far greater importance, is that the thinner the gap the smaller will be the amount of energy dissipated by a voltage sufficient to produce ionization. Thus, at a given overall voltage or surge sufficient to produce ionization there is twice as much energy dissipated in a given area of a five-mil gap as there is in the same area of a $2^{1}/_{2}$-mil gap. This means that in a five-mil gap there is twice as much energy ready to bombard and to start to burn the oil and paper or to find a path around the paper.

Undoubtedly the greatest practical advantage gained by the combined use of strand shielding and thin tapes is the improvement in pulse strength. This is important, since at the higher operating voltages it is well known that the normal 60-cycle working stress is not a limiting factor in determining minimum insulation thickness but that the latter will depend upon the required impulse strength to resist lightning and switching surges. The arguments against the use of strand shielding and thin tapes presented by Mr. Shanklin therefore would be more convincing, if he had presented data showing their effect on impulse strength. In Mr. Shanklin's previous paper he reports an average impulse breakdown on the basis of maximum stress on five samples of a cable without strand shielding or thin tapes of 2,130 volts per mil. Faucett, Komives, Collins, and Atkinson in their AIEE paper report an average for five samples of over 2,760.

C. T. Hatcher (Consolidated Edison Company of New York, Inc., New York, N. Y.): Mr. Shanklin's paper presents further data on the theory of operation of gas-filled cable. The data relative to the automatic grading action and self-healing action is extremely interesting. Study of these data would indicate that the higher the average stress, without exceeding the initial ionization voltage, is the desirable point at which to operate. Gas-filled cable has been operated on our system with average stresses from 39 volts per mil to 68 volts per mil. Measurements made on cable operated at an average stress of 68 volts per mil for one year indicated that it had lower dielectric loss and power factors than cable operated at 44 and 53 volts per mil average stress for approximately $3^{1}/_{2}$ years. If the difference in period of service is disregarded these data would appear to check the theory that a high average stress is desirable.

The data on cables made with oil of varying viscosities are interesting. Apparently an oil which has low viscosity drains too rapidly for satisfactory results with normal cable. Considering these two characteristics, I would like to know if for vertical runs of approximately 200 feet or greater it would be safe to use a medium viscosity oil to provide self-healing, or should a higher viscosity oil be used to prevent migration and thereby lose some of the self-healing properties.

Herman Halperin (Commonwealth Edison Company, Chicago, Ill.): At the beginning of his paper Mr. Hatcher states: "that if supervision could be maintained over the lead sheaths of solid-type cables a large percentage of electrical failures could be eliminated." Apparently expected elimination of service failures due to admission of air and water at sheath cracks by locating and repairing the break before failure is the main justification for the extra investment expense and slight complication of low-pressure gas-filled cable for operation at 11 to 38 kv.

This assumption is reinforced by the fact that in Chicago, for example, we have not had a single service failure due to deficiencies of the insulation in the 1,900,000 feet of new solid-type cable installed on 12- and 33-kv lines in the ten-year period of 1934–43. Almost one half of this cable was belted three-conductor 12-kv cable, and over 12,000 feet was installed on three 33-kv lines. Most of the remainder was shielded three-conductor cable installed on 12-kv lines.

Of course, the need for special designs of cable varies with the circumstances such as high rates of troubles with existing solid-type cables and limitations on cable size by the internal diameter of the duct. Our rate of failures of 12-kv cables due to all causes has been for several years about three per 100 miles per year. This includes very old cable with poor insulation and deficient sheath, as well as the cable made in the past several years that has given relatively good performance. These data mean that for the present average Chicago 12-kv line from a station to a substation, which is about three miles long, we may expect a service failure once in ten years due to all causes and that for new installations the interval between failures will be longer than ten years. Then the question might be this: "How much extra is it worth to have a cable design with which a failure on a line will occur once in, for example, 20 years instead of once in 10 or 15 years as we may expect with the normal solid-type cable"?

It seems to me that for cable for ordinary distribution voltages, that is 9 to 35 kv, it is essential to obtain simplicity in design and operation and to obtain minimum costs. The pressure to obtain lower costs is as great as ever. The insulation thicknesses now used on our shielded three-conductor cable for 12-kv and 33-kv lines are, respectively, 141 mils and 344 mils. These thicknesses have been demonstrated by many accelerated aging tests and trial field installations with reduced insulation to be adequate for long service. The thicknesses are a little above the thickness that would be used for the low-pressure gas-filled cable. The joints on our lines are filled simply with a compound and sealed. These practices seem adequate and economical for 35-kv and lower voltages, except possibly for special cases of very high elevations or very steep slopes. In connection with the latter, we have many risers of several feet to 30 feet on the 12-kv cable and a few on the 33-kv cable, and in one dry tunnel we have several 12-kv cables which drop down 60 feet; and no particular trouble has developed due to compound drainage in these risers.

In Chicago, as in some other cities, it is common practice to use portions of existing lines in connection with installing new lines. This means that in many instances the apparatus that would be necessary in connection with the low-pressure gas-filled cable would call for extra installations between the ends of the new portion of the line and the substation or station.

It is of interest to note some increase in dielectric losses in the low-pressure gas-filled cable after it goes into service according to Figure 11 of the paper and the supplementary data given by the author in the presentation of the paper. Although undue emphasis may have been placed on the economic aspects of dielectric losses in the past, still it seems that the relative magnitude of losses in cables of various types after being in service a number of years is not a negligible matter.

In Table IV the author assumes that service failures would have resulted from one half of the gas leaks. According to our experience in Chicago and what other utilities, including Philadelphia Electric Company, have reported on sheath cracks that did and did not result in service failures, it seems to me that the ratio should be more like one fifth or less instead of one half as far as solid-type cable is concerned. Sheath cracks occur almost entirely in manholes and are found by inspection and repaired before failure occurs.

As brought out in other papers at this session and in previous AIEE papers, a very fruitful method for obtaining a low rate of sheath openings is to have proper manhole design, including adequate offsets between the duct mouth and the joint, and to use good installation and maintenance practices.

A. F. Gambitta (Phelps Dodge Copper Products Corporation, New York, N. Y.): Low-pressure gas-filled cable first applied in the field by Consolidated Edison Company in 1938 is now beyond the experimental stage and is firmly established by the extensive field data given in Mr. Hatcher's paper.

As for the contents of the paper, I may say that it answers every operating question which the cable industry wanted to know. The major questions are:

1. Has any cable failure been experienced? The answer is no.
2. How much cable has been installed, and for how long has it been in operation? The answer is 138,075 feet in the Consolidated Edison Company system alone and over 200,000 feet in all systems with six years as the maximum time of operating experience.
3. Has there been any deterioration of the cable during service? The answer is no.
4. Is the supervision of the lead sheath a foolproof characteristic of gas-filled cable? The answer is yes.
5. Are all the accessories developed to the extent that they are considered satisfactory? The answer is yes.
6. Has there been any trouble caused by migration of compound? The answer is no.

In the paper Mr. Hatcher says that in general leaks have developed shortly after the cable has been in operation. It would be interesting to know what gas-pressure test was made in each line before it was placed in service. The 38-kv low-pressure gas-filled cable manufactured by Habirshaw Cable and Wire Division of Phelps Dodge Copper Products Corporation and installed in 1943 by Virginia Electric and Power Company at Norfolk, Va., was subjected to a rather severe gas-pressure test after the installation work was completed and before it went in service; namely, 20 pounds per square inch for 24 hours without any drop in pressure. Consequently, no gas leaks nor any kind of trouble whatsoever has been experienced with this cable during its first year of service.

It also would be interesting to know the details of cable arrangement and manhole sizes of the 13.2-kv Sherman Creek feeder where buckling and cracking of the lead sheath adjacent to the joint wipes were experienced. Only by knowing the actual cable installation conditions besides the load conditions may we be able to explain the real cause of such troubles and how they could be avoided in future installations.

It is apparent that gas-filled cable does not eliminate lead-sheath trouble to such an extent that it can be installed in any small manhole. I believe that gas-filled cable requires manhole sizes as large as for solid-type cable. The gas-filled cable, however, gives an alarm signal when a gas leak occurs and, therefore, eliminates electric failures due to lead trouble. Also for this very reason, it is the most suitable type to be used in existing ducts with small manholes which was the type of location for the 38-kv low-pressure gas-filled cable in Norfolk, where every possible attention was given in order to anticipate and, therefore eliminate as far as possible, lead-sheath troubles during operation.

Finally, I want to say that the gas-flow direction indicator described in this paper is a very useful device for the maintenance of gas-filled cable lines.

L. F. Hickernell (Anaconda Wire and Cable Company, Hastings-on-Hudson, N. Y.): Figure 11 of Mr. Hatcher's paper shows that after one year in service the power factor at rated operating temperature (74 degrees centigrade) rose from 0.46 to 0.62 per cent or an increase of 35 per cent and the dielectric loss from 0.18 to 0.20 watt per foot or an increase of 11 per cent. It will be interesting to learn if these rates of increase continue in subsequent years of service.

It is difficult to reconcile this aging characteristic with the statement: "Visual examination and power-factor measurements of the paper tapes from the cable did not reveal any indication of deterioration." Ordinarily, radial (tape-by-tape) power-factor characteristics are more useful in the study of aging or deterioration.

If this insulation deterioration and the oxidation products in the five tapes next to the conductor are of any concern, it is suggested that consideration be given to incorporating the carbon-black principle in subsequent designs of this type of cable.

Joseph Sticher (The Detroit Edison Company, Detroit, Mich.): The paper by Mr. Hatcher gives a very interesting account of the operation of low-gas-pressure cable, and brings out the advantages gained by the automatic sheath supervision that can be obtained with this type of cable. Table IV of the paper gives the "estimated failure rates which would have obtained if gas-filled cable had not been used and one half of the gas leaks found in cable sections and joints had resulted in failures during this two-year period of operation." I cannot help but wonder what is the basis for the assumption that one out of each two gas leaks might result in failure. When Tables III and IV are compared, it would appear that one mile-year of gas-filled cable is included in the figures for 1941, and ten mile-years for 1942; also, that in the one mile-year for 1941 two leaks were encountered in the cable, and in the ten mile-years for 1942 four leaks. Now it seems reasonable to assume that sheath leaks that occur in gas-filled cable also would occur if the cable were of the solid type and installed under the same conditions, the only difference being that the existence of the leaks in the gas-filled cable would be known. In that case, the four known leaks in 1942 in ten mile-years of gas-filled cable might correspond to 101 leaks in the 253 mile-years of solid-type cable under consideration. There were, however, only nine failures in the 253 mile-years, or a possible failure-to-leak ratio of 1 to 11. It is not intended to establish a definite failure-to-leak ratio but rather to point out that a ratio of one half is likely to be too severe. The failure-to-leak ratio is important, since it can be considered a criterion of the significance of automatic sheath supervision, that is, the larger the ratio, the greater would be the desirability of obtaining sheath supervision.

This consideration leads us to the economic aspect of low-gas-pressure cable. Although it is realized that there are certain cases in which continuity of service must be maintained regardless of cost and in which reduction of service failures may decrease stoppage of critical processes and prevent loss of good will, a question arises as to what is the more economical course in general: to install a solid-type cable without sheath supervision and repair cable failures whenever they occur, or to use low-gas-pressure cable with sheath supervision and locate leaks whenever they are indicated and repair the sheath to prevent cable failure. There is, naturally, a multitude of considerations that make up the complete economic analysis of the low-gas-pressure cable, but it would be enlightening to have a comparison between this and solid-type cable, both as to unit over-all installed costs and as to cost of maintenance.

The low-gas-pressure cable is a relatively new type of cable, and many questions remain to be asked and answered before a clear picture of the ultimate possibilities of this cable will be available. It is in this direction that such papers as those presented by Mr. Shanklin and Mr. Hatcher are doing a real and worth-while job.

T. F Peterson (American Steel and Wire Company, Cleveland, Ohio): Technical papers which include results of tests and extended theories of operation of cable usually provoke much discussion. By contrast, papers of the type presented by Mr. Hatcher—largely factual—although not leading to lengthy discussion, are none the less extremely valuable. Operations under normal conditions automatically integrate all influences and effects which normally precipitate failure or lead to ultimate destruction of the item under observation. Mr. Hatcher has studied carefully the operation of low-pressure gas-filled cable and has indicated the trends in basic characteristics during operation. All of this should prove very helpful in evaluating the intrinsic worth of the design.

In explaining what appears to be an unusually high rate of sheath breaks he indicates that "the same sheath failures would have occurred in solid cable of the same conductor size and so forth and would, without doubt, have been responsible for a number of electrical failures." I fail to see the justification for this statement, especially since sheath breaks in gas-filled cable may well be caused by inherent characteristics tending to precipitate these failures. I have in mind the possibility that, because of the higher thermal resistivity from copper to sheath, greater differential temperature rise of copper and lead may develop in gas-filled cable than in the case of solid or oil-filled cable, and this ultimately may result in sheath breaks.

G. B. Shanklin (General Electric Company, Schenectady, N. Y.): Mr. Hatcher has made available for the first time operating records of low-pressure gas-filled cable from which the performance of this type of cable can be evaluated. Three pertinent conclusions can be drawn from Mr. Hatcher's paper:

(*a*). Uniformity of insulation quality can be maintained in service, and insulation deterioration of a kind that might lead to service failures has not occurred.

(*b*). Operating experience to date has proved the self-supervising feature of gas-filled cable. Leaks from sheath damage or other causes are controlled by maintained gas pressure and can be detected, located, and repaired before other damage develops or service interruption occurs.

(*c*). Under severe conditions sheath fatigue due to cable movement can occur, and its prevention needs the same careful attention that is necessary with other types of lead-sheathed cable.

The fact that sheath-fatigue cracks due to excessive cable movement can be detected and repaired before serious damage is done does not justify the use of gas-filled cable under conditions more severe than would be used for other types of cable. It would be much better to avoid conditions leading to sheath fatigue by providing liberal expansion bends in the manholes and by keeping daily-load temperature fluctuations within safe limits. Occasional sheath leaks or other such mishaps then can be kept under control without danger of service interruptions and without undue replacement and maintenance costs.

G. B. McCabe (The Detroit Edison Company, Detroit, Mich.): I concur in the remarks by my colleague, Mr. Sticher, in commending Mr. Shanklin and Mr. Hatcher for their excellent contribution to the literature in the gas-filled-cable field. In addition to the points of discussion presented by Mr. Sticher, it is felt that further consideration should be given to statements by Mr. Shanklin condemning the employment in gas-filled cable of high-viscosity impregnating compound and questioning the advantages of strand shielding and graded insulation, especially in view of the satisfactory operating experience for $2^{1}/_{3}$ years of The Detroit Edison Company with a seven-mile circuit of 120-kv high-pressure gas-filled cable incorporating all three of these types of design.

This cable,[1] the insulation of which consists of paper tapes preimpregnated with a compound having a viscosity three times as much as that of the most viscous compound used by Mr. Shanklin in his tests, namely, 3,000 Saybolt universal seconds at 100 degrees centigrade, was designed to be entirely free of ionization up to about twice operating voltage. In the absence of ionization there is no need for ionization-extinguishing or self-healing effects in the cable, and, accordingly, it makes no difference what the viscosity of the compound is. In stating that high-viscosity compounds are undesirable and should not be used in gas-filled cable, it would seem advisable for Mr. Shanklin to qualify such a statement by restricting it to mass-impregnated cables operating at much lower voltages and pressures than 120 kv and 200 pounds per square inch, respectively.

Mr. Shanklin's load-cycle endurance tests have strengthened his opinion that no particular benefits accrue from such expedients as strand shielding and graded insulation in which the thinnest papers are used in the high-stress zone. Mr. Shanklin apparently has overlooked another very important characteristic of a cable, which in operation is subject to lightning and switching surges, namely, impulse strength. In papers presented by the writer before the transmission and distribution committee of the Edison Electric Institute in Atlanta,[2] and in Detroit,[3] the results of impulse tests conducted on three-conductor, 24-kv, type-H cables with and without strand shielding showed conclusively an improvement in average impulse stress of from 17.5 per cent in the case of poorly applied strand shielding to 35 per cent in the case of well-applied shielding. This improvement is largely due to the fact that the conductor shielding tapes tend to eliminate the effect of individual strands and to give the conductor more nearly the surface characteristics of a smooth copper rod with consequent higher-impulse breakdown values. Such construction should, of course, be just as effective in improving the surge strength of gas-filled cables as solid-type cables.

In reference to the effect of grading on impulse strength of 120-kv gas-filled cable, impulse tests were conducted on several samples of cable, identical in all respects except for variations in the thicknesses and densities of individual paper tapes comprising the 600-mil wall of insulation. Test specimens with five-mil tapes of medium density throughout averaged 630 kv on impulse breakdown, whereas several specimens constructed with $2^{1}/_{2}$- and 3-mil tapes of high density in the inner 20 per cent of the insulation could not be broken down by the impulse generator, the capacity of which was in excess of 900 kv.

References

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2. Impulse Tests on Various Kinds of 4,800- and 24,000-Volt Cable, G. B. McCabe. *Edison Electric Institute Bulletin*, volume 8, 1940, pages 474–8.

3. Minutes of transmission and distribution committee. Edison Electric Institute, Detroit, Mich., February 17–19, 1941.

L. I. Komives (The Detroit Edison Company, Detroit, Mich.): We consider the development of the low-gas-pressure cable a distinct advance in the art of cable making and therefore welcome these interesting papers on the subject.

The advantages of the low-gas-pressure cable over the solid type fall in two classes:

1. Improvement of the insulation.
2. Supervision of the lead sheath.

The problem of the insulation is discussed by J. Sticher of The Detroit Edison Company. From his discussion it is apparent that we do not agree with some of the claims which Mr. Shanklin makes for the low-gas-pressure cable. However, we agree with Mr. Shanklin that some positive gas pressure in the cable is better than very low vacuum, and as operators of an extensive cable system, we are very much in favor of a lead-sheath monitor system, which the low-gas-pressure cable system provides and which is described by Mr. Hatcher in his paper. The use of low gas pressure in newly purchased cable appeared to have such decided advantages that we were wondering whether existing solid-type cables already in service could not be benefited by it.

We have experimented with nitrogen feeding of solid-type 24-kv shielded cables in service since January 1942. This experimenting is still in progress but the data on hand permit the drawing of some tentative conclusions and some speculating.

We shall list first the tentative conclusions:

1. It is not difficult to feed short (manhole to manhole) lengths of shielded cables with nitrogen gas, provided there is a fairly uniform distribution of compound. The poorer the impregnation of the cable and the higher the vacuum in the cable during off peak periods, the easier it is to fill the cable with gas.

2. Over a longer distance (1,000 feet or more) there is an appreciable pressure drop. At the feeding point one might have 12-pounds-per-square-inch pressure while 1,500 feet away the pressure is three to four pounds per square inch. This finding makes the sheath supervision on solid cables in service rather problematical.

3. Joints filled with plastic compounds, which are constructed in such a way as to provide a gas channel through the center filler space, will not interfere with the transmission of the gas.

4. On a lightly loaded line a pressure of 25 pounds per square inch must be used before the gas will travel to distances of 1,000 feet or more. However, if a path is established, a lower pressure may be sufficient.

5. The gas must be conveyed to the center of the cable or at least it must be made sure that there is no stoppage near the lead sheath. Nipples penetrating the lead sheath only may or may not be reliable gas channels.

6. The employment of helium in place of nitrogen resulted in no advantages as far as gas travel is concerned.

Let us now speculate how an existing solid-type cable system may be improved by the judicious use of low-gas-pressure cables. Suppose there are several feeders between a power plant and a substation, which are getting old and have high dielectric losses. Instead of replacing most or all of these feeders, suppose we pull out one and replace it with a low-gas-pressure cable. Gas in this low-gas-pressure cable then will be available at every manhole to be fed into the adjacent solid-type cables. One does not have to make such connections in every manhole. Depending upon the flatness of the terrain and what degree of perfectness desired, gas connections at every 1,000 or 1,500 feet only are conceivable. The low-gas-pressure cable thus will serve as a gas main for adjacent solid-type cables and will enable the cable users to increase the pressure in the adjacent solid-type cables from a very low vacuum to somewhere near atmospheric. If it is true that the power factor of an old cable operating under vacuum can be improved by filling it with nitrogen gas, the installation of one low-gas-pressure cable line among a number of old high-dielectric-loss cable lines may not only provide added capacity of its own but also may permit the operation of old cable lines for many additional years.

Mr. McCabe of The Detroit Edison Company suggested that it is not even necessary to install a low-gas-pressure cable to supply nitrogen gas at every 1,000 feet or so. A gas reservoir could be installed at the desired locations, or a small copper pipe installed in vacant center ducts might parallel existing cable lines from substation to substation to convey the gas.

C. T. Hatcher: Discussions of my paper have brought out two main questions, namely

1. Is the assumption that one half of the sheath cracks would result in electrical failures, if the cable had been of the solid type, too pessimistic?
2. Does the failure record, on solid-type cable justify the use of gas-filled cable?

Mr. Halperin and Mr. Sticher have questioned the assumption that one half of the sheath breaks in the gas-filled cable would develop into electrical faults. As stated in the paper the probable number of failures is purely an assumption, as the number of failures which would result from openings in the sheath in a two-year period only can be estimated. Data presented by Mr. Schifreen[1] show that on generating-station tie feeders, of all the failures experienced during an eight-year period 46.6 per cent were caused by sheath cracks. From company operating records we know that failures occur when the sheath on solid-type cable is broken. The length of time that these cracks existed before failure occurred and the number of undiscovered cracks which existed or which still exist cannot be established. Local conditions will affect the failure rate since in some locations there may be no moisture to enter the sheath cracks, whereas at other locations the cable may be submerged continually, and failure quickly follows the rupture of the sheath. It was on these facts that the assumption was based.

Mr. Halperin has raised the question of the necessity for a cable which provides greater reliability than the present-day solid-type cable. Our total system cable failure rate is between five and six failures per 100 miles of cable per year which is somewhat in excess of the figures quoted by Mr. Halperin for the Chicago 12-kv system. We have, in general, used gas-filled cable for station outlets and for tie feeders where maximum reliability is desirable. Failure rates quoted by Mr. Halperin are not desirable for installations of this type, whereas an occasional outage on a distribution system may be tolerated. Basically the gas-filled cable system does not cost an appreciable amount in excess of the cost of a similar solid-type cable system. In some cases, it is possible to install gas-filled cable of a larger conductor size than solid-type cable in existing ducts. On this basis it becomes economical to use gas-filled cable.

In answer to Mr. Gambitta's questions the gas-filled cable is subjected to a 20 pound per square inch pressure test for one-half hour on each joint as it is completed. A check for leaks is made at this time on each joint with liquid soap. A pressure-drop test is made on a completed feeder for 16 hours at 15 pounds per square inch, when time permits.

The Sherman Creek feeder was installed in an existing duct system with manholes of a variety of shapes. The type of manhole most frequently found on this line is octagonal with a maximum width of five feet and a length of eight feet. Most of the sheath cracks have occurred in this type of manhole. Cable arrangements vary in the manholes from the worst condition, where the cable is racked through the manhole with only small horizontal or vertical offsets, to conditions, where greater offsets are possible at one or both ends of the manhole.

Mr. Peterson points to the possibility that sheath breaks might occur in gas-filled cable more frequently than in solid-type cable due to the development in the gas-filled cable of "greater differential temperature rise of copper and lead." If his point is that this greater differential in temperature will result in more relative stress between copper and lead, then sheath breaks thus caused could be expected to be distributed uniformly along the cable. We have found that our sheath troubles generally are concentrated adjacent to wipes, and, therefore, it does not appear that breaks would result from the greater differential in temperature rise which he suggests.

Mr. Komives suggested the use of nitrogen gas in old solid-type cables to increase their life. This suggestion appears to have some merit, and we tried it out a number of years ago. The use of gas in solid-type cables, however, has definite limitations. The formation of slugs of cable compound will without doubt prevent gas from flowing through a solid-type cable for any considerable distance. This fact led to the inclusion of a solid-wall tube in three-conductor gas-filled cable after early experience with cable having three spiral metal gas channels. This situation would be improved, if the solid cable were supplied with gas at numerous points along the route as from a parallel gas-filled cable as suggested by Mr. Komives.

REFERENCE

1. CYCLIC MOVEMENT OF CABLE—ITS CAUSES AND EFFECTS ON CABLE-SHEATH LIFE, Clement S. Schifreen. AIEE TRANSACTIONS, volume 63, 1944, pages 1121–30.

G. B. Shanklin: There is a draftsman's error in Figure 7 of my paper that was not discovered in time for correction. The point for low-viscosity oil was plotted wrongly for a viscosity of 33 and an ionization voltage increase of 275 per cent. The correct values are 37 and 175 per cent. This error does not affect the conclusions given, nor does it materially affect the shape and trend of the curve in Figure 7.

As stated in the introduction, my present paper is the third of a series of progress reports covering our studies of gas-filled cable. The conclusions in the present paper are based on the results presented in all three. Many points raised in the discussion disregard the 1938 and 1942 papers. For instance, Mr. Atkinson claims that no test data are presented on extra thin paper tape and strand shielding. He should refer to all three papers to obtain the whole story.

Mr. McCabe and Mr. Atkinson believe that extra thin paper and strand shielding are essential in the design of gas-filled cable. Our test results do not conform as far as mass-impregnated gas-filled cable is concerned. They are, of course, referring to the preimpregnated Beaver-type cable for which this is correct. A distinction must always be drawn between the two types for a clear understanding of characteristics. One has mobile compound and self-healing characteristics. The other does not.

Mr. Del Mar and Mr. Sticher present long-time aging results on solid-type cable that are not in agreement. Lack of control of both pressure and compound migration explains this. The drained cable Mr. Del Mar tested in 1934 was of the solid type and showed some ionization self-healing effect because he had at least some degree of both compound migration and pressure control. The solid-type cable in Mr. Sticher's Figure 1 shows no self-healing effect, as he made no attempt to control either migration or pressure. On the other hand, the evacuated sample in his Figure 4 does show self-healing effect because of better pressure and migration control. On the same basis the nitrogen filled sample in his Figure 4 has no ionization discharge at all at 77 volts per mil and shows complete stability.

It is not difficult to control pressure inequalities, and thereby migration, in short test samples of solid-type cable. Doing the same thing in long service lengths is another story. There are no gas channels for transmitting pressure and the cross section is too dense to allow this.

Mr. McCabe, Mr. Komives and Mr. Sticher propose to improve old solid-type cable in service by introduction of nitrogen gas pressure. Doubtless this will help in some cases, but the same degree of control obtained in the true gas-filled cable would be impossible. Neither can they expect equally effective, nor even sufficient sheath supervision. When it comes to using one gas-filled cable for controlling parallel runs of solid-type cable, I am not enthusiastic. In the design of the low-pressure type of gas-filled cable only sufficient pressure margin is allowed for maintaining pressure control and exclusion of impurities in that one cable. It cannot be expected to undertake the same burden for several parallel solid-type cables. It would be much better to use separate gas tanks at each joint or a gas pipe manifold connected to each joint of the solid-type cable lines, if conditions warrant this extra expense and complication.

Mr. Del Mar, Mr. Sticher, Mr. Wyatt, and Mr. Atkinson question the theory I have offered that self-healing effect in gas-filled cable is chiefly due to capillary action and a drawing of the mobile compound into the high-stress field by electrostatic attraction. The first two gentlemen have advanced countertheories and all of them, as well as others, have questioned the results with high-viscosity hydrogenated oil, suggesting imperfect initial impregnation and inherent instability of this type of oil. A study of Figures 5 and 6 will show that, at least in this particular test length, the initial impregnation was excellent and the hydrogenated oil did not show inherent instability in the presence of ionization discharge. In fact, this test length withstood severe ionization for 300 days without failure or tendency toward cumulative action.

Conclusions about the lack of self-healing effect in cable impregnated with high-viscosity compound are not based on this test length alone. We have tested all available types of high-viscosity impregnants, including rosin blends, soluble synthetics, and others, in the forms of mass-impregnated cable, preimpregnated cable, and pretreated paper tape for reinforcement wrappings in accessories. Without exception there has been a marked lack of self-healing effect with impregnating compounds having viscosities above 250 Saybolt at 100 degrees centigrade. On long-time endurance test failure takes place only a little above the initial ionization voltage with these extra-high viscosity compounds.

If those advocating the preimpregnated type of gas-filled cable can present overvoltage load-cycle aging data that contradict this conclusion, I would very much like to see them. So far, such aging data have not been presented in the United States nor abroad. Mr. Atkinson states that self-healing effect is not important in his type of high-pressure gas-filled cable, because it operates well below ionization voltage. The latter is correct. In fact, it is of vital importance to make sure that this type of cable operates well below ionization voltage at all times and under all conditions. This can be done only by maintaining a high excess-gas pressure in the order of 200 pounds per square inch.

We have verified a number of times that, other things being equal, self-healing effect is related to viscosity (mobility) of the impregnating compound in gas-filled cable. Capillary and electrostatic attraction appear to explain this better than other theories. Mr. Sticher suggests compound expansion movement under load cycles. This might be a factor, but it should be noted that the cable Mr. Del Mar subjected to overvoltage test in 1934 showed self-healing effect without load cycles. Mr. Del Mar suggests a conducting surface layer of wax in the voids. This also might be a factor, but it does not explain the observed relationship of self-healing effect and viscosity of compound. Neither does it explain the fact that the voids often are completely filled with wax after long-time overvoltage tests.

Mr. Del Mar's Figure 2 was taken from his Roanoke, Va., paper of November 1943. The viscosities of the two impregnating oils were respectively 100 and 200 Saybolt at 100 degrees centigrade, too close together to throw any light on self-healing effect as a function of viscosity. Likewise, the initial ionization voltage of these cables was only 11.5 kv at ten pounds per square inch, corresponding to 52.5 volts per mil. This indicates lack of control during the treating process and is not representative.

Another question raised by Mr. Del Mar relates to automatic grading at voltage stresses below the ionization voltage. All of our tests were made at overvoltages, but I would refer Mr. Del Mar to the interesting test results on removed service lengths of 15-kv and 27-kv gas-filled cable as given in Mr. C. T. Hatcher's contemporary paper. Neither of these removed lengths showed any signs of ionization. Initially, before shipment from the factory, they both had an ionization voltage of 75 volts per mil at ten pounds per square inch. The 27-kv cable retained this ionization voltage after a period of service during which it operated at a stress approximately ten per cent lower. The 15-kv cable, however, shows an ionization voltage after service that is somewhat lower than the initial value and is also approximately ten per cent above the service stress. These results would indicate an automatic grading action during service, corresponding to the respective service stresses. Additional data should clear up this point more definitely in the future.

Mr. Peterson inquires about the thermal and mechanical characteristics of gas-filled cable. The measurement of thermal resistance of the insulation is a routine by-product of the numerous long-time load-cycle tests we have made. In no case have we found compound bleeding to be excessive in standard cable, or to affect materially the thermal resistivity of the insulation. In average cases the resistivity seldom exceeds 700, which is a typical value for old solid-type cable.

As regards mechanical characteristics, I assume Mr. Peterson is referring to sheath fatigue due to cable movement in service. In this respect, gas-filled cable is no different from any other type of lead-sheath cable, with the exception that the internal pressure is always under control. There is no proof, however, that this is of sufficient help to warrant exceeding the same safe limits of cable movement established for solid-type cable.

Mr. Peterson's remarks about the exact balance required, and the compromises necessary, in the design of gas-filled cable are obviously humorous. I would like to ask Mr. Peterson if he knows of any design of high-voltage cable, worth its salt, that does not require the same exhaustive study and close attention to detail.

Mr. McCabe's statement that strand shielding improves the impulse strength of impregnated paper-insulated cable from $17^1/_2$ to 35 per cent is not confirmed by any test data I have seen from other sources. The effect of maximum stress on impulse strength of cables is dealt with in a contemporary paper by Mr. Halperin and the present author.[1] An analysis of those results does not confirm that maximum stress plays such a prominent part.

All points raised by Mr. Richman have been answered in the foregoing relating to extra-high-viscosity impregnating compound and the effects of strand shielding and extra thin paper on impulse strength. It should again be pointed out that distinction should be drawn between the different characteristics of these two types of gas-filled cable.

Mr. Hatcher wants to know if higher-viscosity impregnating oil would not improve compound migration in long vertical runs of gas-filled cable. This is best answered by actual field experience for several years with a 250-foot vertical riser of 27-kv low-pressure gas-filled cable in Mr. Hatcher's system. This cable was impregnated with standard compound having a viscosity of 100 Saybolt at 100 degrees centigrade. The amount of compound bleeding as determined by removal of surplus compound at the lower end of this vertical riser has been no more than experienced on gradual slopes or level ground, and the cable has operated successfully in all respects. The answer is that compound bleeding in gas-filled cable is very different from compound migration in solid-type cable, because all surplus compound not held in the insulation by capillary attraction has already been removed. There is, accordingly, no building up of head pressure along the vertical length and no more tendency to bleed compound into the gas channels than occurs in horizontal lengths. This is best explained by pointing out that all such bleeding into the channels is radial and not longitudinal. Theoretically, a higher viscosity compound always looks better for such vertical risers, because the factors just outlined usually are ignored. Actually, it is only necessary to have a compound of sufficient viscosity to be held in place by capillary attraction. A higher viscosity accomplishes little, if any, more in this respect.

Mr. Hickernell suggests that an impregnating compound with maximum waxing characteristics would be the most desirable for gas-filled cable. Unfortunately, such compounds have always proved the most unstable and the most likely to fail quickly on overvoltage test due to cumulative ionization damage.

Mr. Siegfried's comments on Paschen's law are very helpful and undoubtedly explain why actual ionization test results on gas-filled cable do not agree with theoretical calculations.

Reference

1. Impulse Strength of Insulated-Power-Cable-Circuits. Herman Halperin, G. B. Shanklin. AIEE Transactions, volume 63, 1944, pages 1190–7.

Insulation Temperature Standards for Industrial-Control Coils

Discussion of paper 44-140 by B. W. Erikson, presented at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944, and published in AIEE TRANSACTIONS, 1944, July section, pages 546–8.

G. H. Garcelon (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Mr. Erikson's paper brings out a condition that certainly requires some action. The performance record of industrial-control coils has been good in spite of the fact that the prescribed temperature limits are in excess of those recognized for rotating machinery. This record also includes many years when the insulating materials and processes were inferior to those in use today, and, with the advent of the new materials and techniques, it is definitely in order to develop standards permitting higher temperature rises.

This standardization work should be carried on systematically and thoroughly by a technical committee, giving consideration to all factors which affect the life of coils in actual operation. This work also should take into consideration the undesirability of having a large number of permissible temperature levels, if economic production is to result.

We have been doing a great amount of research work with the silicones, and there is a very definite indication that materials of this class are suitable for continuous operation at considerably higher temperatures than those suggested in the paper. This would result in a third additional temperature level if the other two suggested in the paper were adopted without further consideration. Without any desire to delay taking advantage of these new materials, I do think that considerable caution should be exercised before setting up new standards on such an important subject.

The Degree of Short-Circuit Protection Afforded Small Low-Voltage A-C and D-C Starters by Means of Fuses and Circuit Breakers

Discussion and author's closure of paper 44-139 by B. W. Jones, presented at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944, and published in AIEE TRANSACTIONS, 1944, August section, pages 611–12.

G. H. Garcelon (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): This paper by Mr. Jones clarifying his previous AIEE paper on the same general subject is beneficial in that it definitely should eliminate the misunderstanding held in many places that National Electrical Code fuses were recommended for interrupting short-circuit currents up to 100,000 amperes. Although we believed that no such recommendation was intended, we received many assertions to the effect that they were being recommended for such service. As a result of this controversy numerous tests were made on several makes of National Electrical Code fuse, and we found nothing to indicate that any make of National Electrical Code fuse was good for much more than its Underwriters' Laboratories rating of 10,000 amperes.

We still disagree with the recommendations in the present paper. If the short circuit occurs at a point where the impedance of the fuse, the starter, and the overload relay heaters are all in series with the fault current, it is possible that satisfactory operation may be obtained. We have, however, no assurance that the fault always will occur at such a point, and, if a short circuit should occur elsewhere within the starter, the current would be much higher; and serious damage to the starter and possibly to personnel would result.

When tests on circuits of various current capacities were made, it was found that much worse results were obtained when the starter was closed on the short circuit than when the short circuit was produced with the starter closed. This is probably caused by the bouncing of contact resulting in a short circuit at the contactor, thus cutting out the impedance of the overload relays, the contacts, and some of the internal wiring.

National Electrical Code fuses are tested and approved on the basis of Underwriters' Laboratories, Inc., specifications on a circuit which will deliver 10,000 amperes when it is short-circuited ahead of the fuses. Some fuses may have higher interrupting capacity than indicated by this specification, but others can and will be found on the market which have very little excess capacity; therefore it seems to us that it is not good engineering to apply standard National Electrical Code fuses on any circuit having a greater available current capacity than provided for in the Underwriters' specifications.

Philip N. Ross (nonmember; Bureau of Ships, Navy Department, Washington, D. C.): This paper has corrected satisfactorily the erroneous impression created by Mr. Jones's previous paper[1] with regard to the interrupting ability of 250- and 600-volt National Electrical Code fuses. However, the author's two major conclusions, namely, that (1) fuses are entirely superior to circuit breakers in providing short-circuit protection for motor controllers and (2) that such protection must be located in the controller are not clearly and completely supported by the data presented in the paper.

In Figures 3, 4, and 5 of the paper, it will be noted that the fuse and circuit breaker being compared in each case have equal ratings. This is not a sound basis for comparison, since, for example, in an a-c motor circuit the fuse size required is usually equal to at least 400 per cent of the motor rating, whereas a circuit breaker may have a rating equal to that of the motor. Thus, the I^2t value of 288,000 shown in Figure 3 of the paper for a 25-ampere circuit breaker should be compared with the I^2t value of 170,000 shown in Figure 5 of the paper for a fuse. It is evident that under this condition the fuse is far less superior with respect to the circuit breaker in limiting the energy dissipated in the short circuit than is implied in the paper. It should be noted also that as the fuse size becomes larger the I^2t value increases more than proportionately, so that for fuse sizes of approximately 150 amperes or more the circuit breaker is very definitely superior from the standpoint of limiting the energy dissipated in the fault.

The relative merits of the fuse and the circuit breaker are further distorted in the paper by placing all of the emphasis on extremely high values of available short-circuit current, that is, 50,000-amperes alternating current or 23,000-amperes direct current. It is well known that it is quite unusual, if not impossible, to obtain currents of these magnitudes on low-voltage circuits of small sizes such as those supplying motors of the horsepower ratings discussed in the paper. To illustrate this fact we need only consider that the impedance of a 4,500 circular mil cable capable of carrying 20 amperes is 0.15 ohm per 50 feet, giving a short-circuit current of only 1,730 amperes or 86 times rated current when it is connected to an infinite 450-volt a-c bus. When the impedance of the starter also is included in the circuit to the fault, it is evident as shown in Figure 2 of the paper that a further substantial reduction in the actual short-circuit current takes place. Insofar as short-circuit protection of the starter is concerned, the current to a fault on the load side of the starter should be used in making the comparison of fuses and breakers.

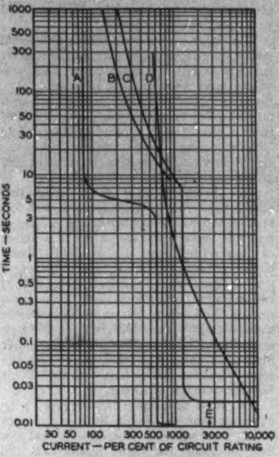

Figure 1. Co-ordination of protective devices on a single-motor-load circuit

Curve *A*—Induction-motor starting current
Curve *B*—Motor overload-relay time–current characteristic
Curve *C*—Circuit-breaker time–current characteristic
Curve *D*—Fuse time–current characteristic
Curve *E*—Circuit-breaker operating time

The significance of the actual values of short-circuit current in determining the relative performance of circuit breakers and fuses in the protection of motor controllers can be seen by reference to Figure 1 of this discussion. This figure shows the time-current characteristics of a fuse and a circuit breaker when they are applied to a single-motor circuit and clearly demonstrates that for short-circuit currents up to 80 times the circuit rating the breaker is faster than the fuse. As previously stated this is the current range wherein most, if not all, of the short-circuit currents will be for faults on the load side of motor controllers. Thus, except in cases where the length of circuit to the controller is extremely short or the rating of the overload relay is appreciably below the minimum available circuit-breaker ampere rating, a circuit breaker provides equal if not superior protection to that of a fuse.

The possibility of operating a three-phase motor on single phase, when fuses are used for short-circuit protection, also should receive attention in making the comparison between breakers and fuses. Because of the lack of consistency and uniformity in the time-current characteristics of low-voltage fuses, blowing of only one fuse often may occur on a line-to-line fault on a three-phase system. The resulting single-phase operation usually is not readily detected by personnel and will soon injure motors operating under load.

Sound practice in fault protection on low-voltage systems requires that each motor branch circuit be provided with short-circuit protection at the branch point. Consequently, the addition of a short-circuit protective device in the motor controller means that two devices have been installed in the same circuit for the same purpose. The apparent reason for advocating such an arrangement in this paper was to insure that the fuse, which the paper has concluded to be essential for protection of the starter, will not be applied where its interrupting capacity is exceeded. However, as previously stated, the superiority of the fuse is questionable. Therefore the desirability and need for providing short-circuit protection integral with the starter in addition to that which should normally be provided at the branch point of the motor circuit are also of doubtful value. The use of circuit breakers at the circuit-branch point will in the majority of cases provide adequate protection for the starter, and, since breakers unlike fuses can be operated successfully in cascade, it is unnecessary to be concerned over the fact that the available short-circuit current may exceed the interrupting rating of the breaker. The practice of using fuses in motor controllers was discontinued by the United States Navy several years ago. Service experience following this change has been very satisfactory and has proved that little, if any, loss in the degree of protection afforded the starters was incurred by removal of the fuses.

In view of the foregoing considerations and in the absence of more complete and accurate data covering a wide range of current values it is not believed that any sound conclusions can be drawn relative to the superiority of fuses over circuit breakers in protecting motor starters under short-circuit conditions.

REFERENCE

1. THERMAL CO-ORDINATION OF MOTORS, CONTROL, AND THEIR BRANCH CIRCUITS ON POWER SUPPLIES OF 600 VOLTS AND LESS, B. W. Jones. AIEE TRANSACTIONS, volume 61, 1942, July section, pages 483–7.

B. W. Jones: Mr. Ross questions the conclusions about starters in this paper regarding the superior short-circuit protection that fuses provide as compared with that provided by small circuit breakers and points out that the data presented in Figures 3, 4, and 5 are not a sound basis for comparison because a 30-ampere fuse is compared to a 25-ampere breaker.

In dealing with short-circuit protection for a line of starters it is essential that we protect the starters in all applications. This includes the ones that are the hardest to protect, as well as those that are less difficult to protect. The motor sizes that would be used in conjunction with the smallest frame size of breaker (50-ampere frame, but having several lower trip-coil ratings) would cover approximately the whole range of motors that are under discussion down to the smallest size. Let us assume the latter to be one horsepower. If fuses were used for this same range of motors in place of the circuit breaker, their sizes would range both below and above the 30-ampere fuse size that was used for comparison with the 25-ampere breaker in Figure 3. For the sake of brevity only one size fuse was used per breaker, and that was the one nearest the breaker rating.

It should be noted that the 25-, 50-, and 100-ampere breakers in Figures 3, 4, and 5 all required approximately 12 milliseconds to interrupt the current. It therefore follows that, if the 25-ampere breaker was fitted with a five-ampere tripping coil, it would not interrupt the circuit any faster than 12 milliseconds. It is thus obvious that, if the motor was of such a size that it required a 15-ampere fuse to give it full protection, *then any size circuit breaker that could be selected in place of the 15-ampere fuse would allow an even greater ratio of I^2t values than that shown in Figure 3.* Therefore, the values shown in Figure 3 are really too conservative, if we consider the worst condition instead of the average which all three figures were intended to convey. The same can be said for the remaining figures. It should be noted also that even the 100-ampere fuse allowed a smaller I^2t than did the 25-ampere breaker. Therefore, since the fuse shows a superior short-circuit protecting ability, even when the most adverse ratio of fuse to breaker size is selected and since this superiority increases as the fuse size diminishes relative to the breaker size, I think that sufficient supporting data were submitted to warrant the conclusions.

Mr. Ross further points out that, as the size of the fuse and the starter increases, the ratio of the I^2t values decreases. This is very true, as was pointed out in the original paper. It should be noted that this is the basis upon which the size of the starter was limited to size three (50 horsepower for 440 and 550 volts) where fuses were recommended because of their superior short-circuit protection. Above this size of starter the abilities of the breaker and the fuse become sufficiently similar, so that there is no occasion to select either one in preference to the other because of any superior protecting ability.

Mr. Ross also says that "the relative merits of the fuse and the circuit breaker are further distorted by placing all the emphasis on extremely high values of available short-circuit current." As previously pointed out, when short-circuit protection is being studied, as much consideration is not given to the less difficult part of the problem as is given to the part which may cause the most destruction. If some installations have 50 feet of number 14 wire in series with them, which limits the current to a low value such as 1,700 amperes, that simply classifies the installation as one of the less difficult ones, which perhaps can be protected by either fuses or circuit breaker. But, when a general application is being considered, it is essential that the more difficult parts of the problem be considered. In today's industries, 10,000, 20,000, or 50,000 available amperes are fairly common; many installations are coupled closely to the bus supply; and the smallest size of motors may be connected to the larger power supplies.

The value of currents which the small starters are likely to receive during a short is shown in Figure 2 of the paper, and these values have been verified by tests. Also, the lower end of this curve (below five horsepower) shows values that would be delivered from a small power supply, approximately 500-kw rating, which means that the circuit breaker is too slow to protect this group against even this small source of power. From another short-circuit test record taken on a 25-horsepower 550-volt starter which was protected by means of a circuit breaker and connected to a source of power that had an available short-circuit current of 24,000 rms amperes, it was learned that approximately 13,000 rms amperes got through each of the three phases, but a second test on the same device showed 20,000 rms amperes got through one phase with less currents in the other phases. I think that this shows that the relative merits of the fuse and the circuit breaker were not distorted.

Mr. Ross submitted his Figure 1 as evidence that "a circuit breaker provides equal, if not superior, protection to that of a fuse," because the figure shows that the breaker is faster than the fuse between 13 and 80 times the normal current rating of the motor and because, he says, that this range is where most short-circuit currents fall. Let us assume that the data shown in his Figure 1 is correct, and then see whether his is a correct conclusion. My whole paper emphasized that fuses provided this superior protection to only the smaller sizes of motors, such as from 1 to 25 horsepower. Therefore, as an example, let us select a five-horsepower 550-volt three-phase induction motor, which has a full-load current of five amperes, and then, according to Mr. Ross, a short circuit on the motor side of the starter would be limited to from 13 to 80 times five amperes, which is 65 to 400 amperes. Now this hardly can be classified as a short circuit. Actually, according to a number of test records, this range falls between *300 to 600 times rated current or 1,500 to 3,000 amperes,* and for small starters it matters little whether the available short-circuit current of the supply is 5,000 or 10,000 amperes.

There is also another point that should not be overlooked. The weakest thermal link in the starter is fully protected by the fuse over the full range of currents under consideration, and this includes the range from 13 to 80 times normal. Or, saying it another way, this extra protection that is supplied by the breaker over the last mentioned range is not needed. But the range from 80 to 600 times normal current, where the short-circuit values generally fall, is the place where the breaker does not protect this starter as is graphically shown by Mr. Ross's figure. Therefore, I think that Mr. Ross has drawn an entirely erroneous conclusion because of his incorrect assumption that the short-circuit currents fall within this low range of currents, and that the starter needs this extra protection over the 13 to 80 times normal range.

If the circuit breaker will not protect the starters as they are now being made, it does not improve the lack of protection to locate the breakers at the branch point of the power supply. If the breakers are used, then this lack of short-circuit protection should be recognized. But, if full short-circuit protection is essential, then fuses should be used and mounted in the starter. Thus it might be stated that both fuses and breakers have certain limitations; the fuse is limited to being located in the starter instead of at either the starter or at the branch point of the power supply, one of which migh be considered more desirable. The small size breaker is limited in its ability to protect fully the smaller starters against damage during short circuits and also is limited in its ability to protect itself against damage where the available current is above its interrupting rating.

Mr. Ross generalizes by saying that the "practice of using fuses *in the motor controllers* was discontinued by the Navy several years ago and the service experience following this change has been very satisfactory." Now this statement is correct, but it is said in such a way that it would leave the casual reader with the impression that is just opposite to the facts. The facts are: that in general for the 1- to $7^1/_2$-horsepower starters the fuses are still used as the short-circuit protection means, and that the change consisted in removing the fuses from the starter and mounting them back at the distribution center. Circuit breakers are being used to protect motors above this size. Then, Mr. Ross concludes by saying that "in the absence of more complete and accurate data, no sound conclusions can be drawn. . . ." I would like to emphasize the fact that these two articles were based upon a large number of tests that have extended over a period of ten years or more and have been made with the most up-to-date testing facilities and on actual commercial starters. I know of no better basis upon which to rest sound conclusions.

Industrial Control—Shunted-Armature Connection for a D-C Shunt Motor

Discussion and authors' closure of paper 44-141 by G. F. Leland and L. T. Rader, presented at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944, and published in AIEE TRANSACTIONS, 1944, September section, pages 617–19.

H. G. Moore (General Electric Company, Erie, Pa.): The determination of the values of shunt resistance S and series resistance R can be made by an alternative method which is believed to have advantages in certain cases over the graphical method presented by the authors of the paper. This alternative method is based on the fact that the armature circuit, with shunted-armature connections, can be represented by an equivalent simplified circuit comprised of an armature having an effective resistance R_a' and connected to a supply having an effective voltage V' as shown in Figure 1 of this discussion.

Equation 4 of the paper may be written:

$$E=\frac{VS}{S+R}-I_a\left(\frac{RS}{S+R}+R_a\right) \qquad (1)$$

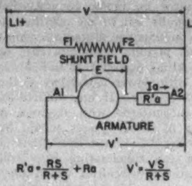

Figure 1

In Figure 1 of this discussion:

$$E = V' - I_a R_a' \tag{2}$$

The speed-current (or speed-torque) characteristic of the motor connections shown in Figure 1 of this discussion would be identical with that of the shunted-armature motor connections if

$$V' = \frac{VS}{S+R} \tag{3}$$

and

$$R_a' = \frac{RS}{S+R} + R_a \tag{4}$$

Since E is proportional to motor speed, the speed regulation per unit armature current (or torque) is proportional to R_a', and bears the same proportion to R_a for the normal connection of the motor armature direct to line voltage V.

The ratio of no-load speed with shunted-armature connection to no-load speed at line voltage is also the ratio of V' to V, or is numerically equal to $S/(S+R)$.

The simplified procedure in calculating the values of R and S, then, is as follows:

1. **Obtain the value of $S/(S+R)$ by determining the ratio of no-load speed with shunted-armature connection to no-load speed with armature connected directly to line voltage.**

2. **Obtain the value of R_a'/R_a by determining the relative speed regulation per unit torque (or per unit armature current) for the two connections. From the known value of armature resistance R_a, calculate the ohmic value of $RS/(R+S)$ from the relation**

$$\frac{RS}{R+S} = \left(\frac{R_a'}{R_a} - 1\right) R_a \tag{4a}$$

3. **Having thus evaluated $S/(S+R)$ and $RS+(S+R)$, solve for R and S by simple substitution.**

The number of calculations involved is no more than required to set up the points on a chart and translate the graphically determined points back into numerical values by the method presented in the paper—and the errors of graphic determination are avoided.

EXAMPLE

Using the same motor and slow-down application calculated by the authors, it is first noted that the internal resistance of the motor is ten per cent normal ohms. Therefore, the no-load speed is 111.1 per cent of full-load speed and

$$R_a = \frac{230 \text{ volts}}{39 \text{ amperes}} \times 10 \text{ per cent} = 0.59 \text{ ohm}$$

The ratio of no-load speeds for the two connections is

$$\frac{66.7 \text{ per cent}}{111.1 \text{ per cent}} = 0.6 = S/(S+R)$$

whence

$$R/(S+R) = 1 - 0.6 = 0.4$$

The specified speed regulation from 0 to 34 per cent armature current with shunted-armature connection is 66.7 per cent − 45 per cent = 21.7 per cent of rated full-load speed. The speed regulation from 0 to 100 per cent armature current at line voltage is 11.1 per cent of rated full-load speed. The relative regulation per unit armature current, therefore, is

$$\frac{21.7}{34} \times \frac{100}{11.1} = 5.74 = \frac{R_a'}{R_a}$$

whence

$$\left(\frac{R_a'}{R_a} - 1\right) R_a = 4.74 \times 0.59 \text{ ohm} = RS/(S+R)$$

Dividing this value of $RS(S+)R$ by the values of $S/(S+R)$ and $R/(S+R)$ determined before,

$$R = \frac{4.74 \times 0.59}{0.6} = 4.66 \text{ ohms}$$

and

$$S = \frac{4.74 \times 0.59}{0.4} = 6.99 \text{ ohms}$$

G. F. Leland and L. T. Rader: We thank Mr. Moore for his discussion outlining an algebraic solution to the shunted-armature connection.

This method is quite applicable when data are given as one speed-torque point and the no-load speed. If the no-load speed is not given but instead two speed-torque points are specified, the graphical method appears to us to offer advantages in time and simplicity. In some cases a certain speed reduction is required but the load is not known at the required speed point. By laying out graphically the speed-torque lines for various conditions of assigned series and shunt resistances, a system having reasonable regulation can be decided on most easily. In general, in industry, loads are not known to any good degree of accuracy and even graphical layouts are usually much more accurate than is warranted by the data.

Modern Cargo-Winch Control for Use on Victory Ships

Discussion and author's closure of paper 44-142 by F. H. Holt, presented at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944, and published in AIEE TRANSACTIONS, 1944, September section, pages 623–8.

D. C. Gray (American Hoist and Derrick Company, St. Paul, Minn.): In this discussion, we wish to comment on the control requirements for the cargo winch listed in the first part of Mr. Holt's paper. In reference to requirements 1 and 5 we believe that it is impossible to match control points to facilitate burtoning, because of the wide range of loads which are handled on the cargo winch. In the process of burtoning cargo on a ship, a slight variation in the path of the load during the transfer across the deck compensates for appreciable differences in the hoisting and lowering speed-torque curves. The fact that successful burtoning is obtained with controls having widely different speed-torque characteristic curves would indicate that the selection of such curves is not a critical matter.

We agree with requirement 2 that rapid acceleration and deceleration are desirable to give responsiveness to the winch. In the latter part of the paper, Mr. Holt discusses deceleration from the viewpoint of motor commutation but does not discuss the effect of full-load deceleration on the ship's rigging. We believe that a study of actual load handling with an oscillograph or accelerometer would disclose transient decelerating rates of such magnitude as to reduce seriously the safety factor in the ship's rigging. The strength of the rigging can, of course, be increased to stand such forces, but it adds weight and cost to the ship. A better solution might be to alter the control sequence to prevent the occurrence of such high rates of deceleration.

For the handling of cargo, we question in requirement 3 the usefulness of the first point lowering speed of 15 feet per minute at full load. Based on our experience in handling materials, we believe that three-ton loads (which is the winch capacity on single line) can be landed satisfactorily at 40 feet per minute. For larger loads, the tackle is changed, and slower landing speed automatically will be provided. Since a very appreciable power loss and consequent resistor heating is required to produce this speed of 15 feet per minute at full load, we believe that the control design could be modified profitably to give the higher landing speed indicated.

Although Mr. Holt indicates a light-line lowering speed of 400 feet per minute in requirement 4, we believe that during actual paying out of light line on the Victory ships, a speed of around 350 feet per minute is obtained. Any consideration of a higher light-line speed should be based on the ability of the wire rope to run through the boom sheaves as fast as the winch drum unwinds it. If the rope does not reeve through the sheaves as fast as it is unwound, it becomes tangled, and considerable time is required to straighten it out.

We agree with requirement 6 that good jogging characteristics are very necessary.

In addition to the requirements which Mr. Holt has given, there are several others which we believe deserve consideration. Since approximately 90 per cent of the winch loads are less than 50 per cent of the winch rating, the aim of the control designer should be to provide for the best performance at the lower loads. The rising speed-torque characteristics of the motor automatically provide this in the hoisting direction, but existing designs are notably weak in the lowering direction. It will be noted from Figure 2 in Mr. Holt's paper that a 2,000-pound load, which by the way is extremely common, has a maximum lowering speed of only 260 feet per minute. We suggest consideration of a control design that will provide a flat speed-torque curve for the high-speed lowering point to give approximately 375 feet per minute at full load and for no load as high a speed as is practical but not less than 325 feet per minute. This change in control would raise appreciably the speed for lowering 2,000-poundloads, and lower slightly the no-load lowering speed, resulting in faster winch performance. With such a design, the load relay with its attendant faults could be omitted.

Load-relay operation is as follows: When the stevedore moves the master switch handle to the full lowering position, the load is accelerated to about 450 feet per minute. At this point the relay operates to slow the load to its normal lowering speed. This slowing down is objectionable to the stevedores, because it gives them the feeling of not being able to control the load.

An additional requirement for good winch service is a resistor having capacity for whatever mishandling the stevedore may give the equipment. We have found from hoist applications on many cranes that the resistor design is not determined by the average requirements but by the occasional misuse of the equipment by the operator. The most frequent abuse is the handling of heavy loads slowly, with the heaviest loads being handled most slowly. This same operating psychology seems to apply to stevedores as well as to crane operators.

We find, too, that in many foreign ports the stevedores are not as skilled as they are in the United States and will not, or cannot, take advantage of the full speed performance built into our winches. Instead they operate on intermediate speed points. Since all winch resistors are of the ribbon type for mechanical reasons, they have very low heat storage; so the only way they can stand this abnormal operation is to have sufficient built-in capacity. Based on such operating requirements, our specification of a winch resistor would permit operation on any master switch point for as long as desired without damage. We know that such performance can be realized, because it is being done.

The overload relay of the control panel has a tripping point of 300 per cent of motor current, having been raised from 250 per cent about three years ago. The reason for this increase is not known definitely to us, but we assume that too many tripouts occurred with the lower setting. We wish to point out that this increase in motor-current trip setting has resulted in heavier and more costly gears and shafts for the cargo winches to meet strength requirements. A modification of the existing control sequences to permit the use of a 250 per cent overload trip setting would be very desirable for any new winch designs.

Mr. Holt neglected to mention one very important part of the winch control: the master switch. This is the only part of the control with which the stevedore has contact, and in many instances his judgment of the winch is based on the convenience and ease of master-switch operation. When the master switch is mounted on the ship's deck near the hatch coaming, the operator may either sit on top of the master switch and lean over the hatch coaming, or stand and lean over the hatch coaming. Either way, he must manipulate master-switch handles which are behind him, and this is very tiring.

To provide reasonable operating comfort for the stevedores, we suggest the following design requirements for a winch master switch:

1. Provide natural up-and-down handle motion instead of back-and-forth motion.

2. Make provision for seating the operator.

3. Whether seated or standing, the stevedore should be able to see into the hatch, and at the same time manipulate the control handles without fatigue.

When such a master switch is available, the naval architect will have the responsibility of properly locating it so that its full possibilities are realized.

Our experience with other types of materials-handling machinery has demonstrated conclusively that operator comfort brings increased daily output. We believe that making it easier for the stevedores will give similar results for cargo handling.

G. H. Garcelon (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): In commenting on Mr. Holt's very interesting paper, I would like to mention an additional very important reason which brought about the consideration of the unit winch. Prior to the use of the unit winch, the control panels and resistors for all winches on a ship were concentrated in two deck houses. Damage to one of these houses would probably render useless one half of the ship's winch capacity.

Some of the earlier history also may be of interest to the members. The first unit winches were streamlined so thoroughly that it was very difficult to service the equipment. The next step resulted in leaving the winch, the motor, and the brake in much the same location as on the old-type winches but in mounting the control and resistor on the winch in watertight compartments practically identical with those illustrated in the paper. Then came the present co-ordinated design as shown in the paper resulting from the joint efforts of the Maritime Commission and several electrical manufacturers and winch builders. With this design any complete winch is interchangeable with another make of winch; the motors used are interchangeable, as are the brakes or the entire combination of brake, resistor, and control panel.

This co-ordinated design now is being manufactured by five or six winch builders, and electric equipment is being supplied by several manufacturers.

F. H. Holt: Mr. Gray questions the need for the matching of motor speeds for burtoning. A close study of the burtoning cycle will show that one winch is often required to handle full load, while the other winch is handling light load. In order easily to transfer load into the ship without excessive manipulation of the master switch, the speeds of the two winches with their different loads must be matched. Although it is not possible or too important to match these speeds exactly, they should be approximately the same. On d-c control for cargo winches the characteristics match up fairly well regardless of the control scheme used; however, on a-c winches it is very difficult to obtain the various light-line speeds. The feature never should be neglected when designing control for cargo winches, if ease of operation is to be considered.

The maximum rate of motor deceleration has been limited by motor commutation and it is generally left to the operator to operate the equipment to protect the ship's rigging. This permits maximum rate of deceleration during emergencies and slower deceleration with slower movement of the master-switch handle. If any difficulties have been encountered with riggings, a careful study of these stresses should be made. It must be remembered that a reduction of the decelerating rate will present operational difficulties, which must be weighed against reduced rigging stresses.

The extremely low speed of 15 feet per minute has been required by a number of the cargo ship operators, but it might be possible to increase this speed slightly. The landing of a load in small craft must be considered when increasing this speed.

The upper limits of light-line speeds have been reached for the reasons Mr. Gray has given, and, until better methods of handling the cable are found, these speeds may not be materially increased.

Mr. Gray has outlined several additional requirements for cargo winch control, and I agree that higher speeds on the fourth point lower would be desirable on the West Coast, but I doubt whether they would be widely used on the East Coast because of the fact that two men are used in the East to operate a pair of winches. This two-man operation materially reduces the operating speeds, and the higher speeds are seldom used. The faults of the load relay have shown up usually with one-man operation as a result of the better co-ordination of the two winches and the resulting increase in speed. The emphasis on higher light-line speeds has come about as a result of the desire for higher cargo-handling rates. The higher speeds do not always result in higher cargo-handling rates because of the long rest time during each cycle.

Mr. Gray has recommended a continuously rated resistor for this application, but past experience on this application does not justify the use of such a resistor. It is true the resistor is not designed on an operating-cycle basis but on the basis of experience. The size of the resistor would have to be increased materially to give continuous capacity, and the increase in cost, weight, and size could not be justified on the basis of past performance.

The increased settings of the overload relays have resulted from faster operation of the master-switch handle and the larger current peaks obtained. This operation is more or less general shipboard practice, and a compromise between speed of response and cost of gears can be established only by the purchaser.

The change in the design of the master switch is a very interesting problem and is made difficult by the fact that a pair of switches may be operated by one or two men. The type of switch, its location, and its operation for this application must be determined by joint efforts of the electrical manufacturer, the Commission, and the ship operators in order to get all viewpoints. This matter should be investigated, and I appreciate Mr. Gray's suggestions.

It should be pointed out that a number of the requirements which Mr. Gray has questioned have been established by the Maritime Commission on the basis of their experience in operating vessels on which such winches are installed.

History and Development of the Electronic Power Converter
(44-143)

The Electronic Converter for Exchange of Power
(44-144)

Design of an Electronic Frequency Changer
(44-145)

Switchgear and Control for an Electronic Power Converter
(44-146)

Pentode Ignitrons for Electronic Power Converters
(44-147)

Discussion and authors' closure of papers presented at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944, and published as follows in AIEE TRANSACTIONS, 1944:

44-143 by E. F. W. Alexanderson and E. L. Phillipi, September section, pages 654–7.

44-144 by F. W. Cramer, L. W. Morton, and A. G. Darling, pages 1059–69.

44-145 by C. H. Willis, R. W. Kuenning, E. F. Christensen, and B. D. Bedford, pages 1070–8.

44-146 by W. N. Gittings and A. W. Bateman, August section, pages 585–9.

44-147 by H. C. Steiner, J. L. Zehner, and H. E. Zuvers, October section, pages 693–7.

S. B. Crary (General Electric Company, Schenectady, N. Y.): The authors of paper 44-144 and its companion papers have done a thorough job in presenting a complete and full analysis of the electronic type of frequency converter, which should make possible rational comparisons with other types of frequency-conversion equipment. The authors have indicated the possibility of using this type of converter to control the power flow between systems of the same as well as unlike frequency. It might be well to review briefly the experience and trend in system design as regards the exchange of power between areas and systems.

Synchronous–Synchronous Tie

Large areas now are served by interconnected systems operating in synchronism which exchange power with a high degree of reliability. An overhead tie line represents probably the most economic type of connection that can be made between systems of similar frequency. Performance of such an interconnection has been improved very greatly during the past 20 years by the development of quick-operating relays, quick-clearing breakers, properly applied tie-line and frequency control, and a more complete understanding and appreciation of system performance and stability during sustained and transient load changes. As a result of this very substantial progress, systems can be connected together by tie-line capacity or synchronous–synchronous frequency-changer capacity, which is relatively small compared to the size of the interconnected systems. For example, some systems can be operated successfully in a synchronous–synchronous relation when the tie capacity is appreciably less than 10 per cent of the smaller-system capacity.

Nonsynchronous Tie

The nonsynchronous tie between systems has the advantage that the load change can be isolated to one system, usually the smaller of the two interconnected systems. This requires that the prime movers of the smaller system be subjected to greater changes in prime-mover output due to the load changes of the smaller systems, than if the systems were tied together in a synchronous relation. This may or may not have advantages depending upon the conditions peculiar to the case under consideration. If the smaller system is a steel mill, for example, with a large rapid varying load, the larger system will be relieved of taking as much or any of the load changes of the smaller system, and therefore the operation of the larger system will benefit correspondingly. However on the other hand, the smaller system will be subjected to greater changes in frequency and the prime movers to greater changes in output. There are available at least two methods of isolating the load changes to the smaller system, such as using a Scherbius frequency changer set or an electronic converter of the type described in these papers.

It can be concluded, therefore, because of the greatly improved performance of synchronously operated systems that the need for a nonsynchronous tie will be limited in general to those areas where it is desirable to insulate from the larger system the objectionable load variations of a smaller system, requiring the smaller system to operate with larger frequency deviations than would be required, if it were operating in a synchronous–synchronous relation with the larger system.

B. M. Jones (Duquesne Light Company, Pittsburgh, Pa.): The Duquesne Light Company's 60-cycle system and the Carnegie-Illinois Steel Corporation's 60-cycle system now operate in parallel, and the converters which tie the steel corporation's 60-cycle and 25-cycle systems also tie the Duquesne Light 60-cycle system to the 25-cycle system of the Carnegie Steel Corporation.

Figure 1 of this discussion shows the Duquesne Light system diagram, and the Carnegie Steel Corporation interconnection is shown in the lower right near a bend in the river. Actually, the 60-cycle load of the Steel Corporation carried on the Duquesne system before these electronic converters were installed has been much greater than the 28,600 kw shown, reaching 60,000 to 70,000 kw at times. The total installed name-plate generating capacity of Duquesne Light is 624,000 kw, with 262,500 kw at Colfax, 180,000 kw at Reed, 116,500 kw at Brunot Island, 60,000 kw at Phillips, and 5,000 kw at Stanwix. Phillips is the newest plant, having been placed in service in January 1943. It was planned and authorized prior to our entry into the war, in anticipation of the large war loads to come, which actually did come, and we take a little pride in this advance planning.

We are very glad to state that these devices are not causing any concern or disturbances at the present time. Some interference did occur on the Duquesne Light Company's system at the time of initially placing the first 10,000-kw converter in service in December 1943, resulting in rather widespread voltage-flicker complaints on our system which, we believe, resulted from sporadic faulty tube operation (commutation failures) which lasted intermittently from the time the original cut in was made at about 9 p.m., throughout that night, the next day, and to a lesser extent spasmodically thereafter. Since that time, the second 10,000-kw unit has been cut in service (on February 15, 1944), and some voltage complaints were encountered, although there were not so many as there were when the first unit was cut in.

It is interesting to note that the electronic-frequency-converter installation requires each system to provide its own reactive current, for the converter installation itself does not do this. The frequency records for our system during the time these frequency-changer installations have been in service and during the initial tryout thereof show no appreciable effect of this interconnection. Figure 13 of paper 44-144, item *d*, shows the 60-cycle system frequency at the time of certain tests on the electronic installation.

One attractive feature of such a frequency converter tying two large systems together is the steady load that can be interchanged between the two systems. The authors' paper, including their exhibits, brings this out very clearly. The ideal condition of an electric interconnection is for the power flowing over the interconnection to be at absolutely steady load. This same condition is desirable for any load, but the fluctuating nature of most loads is inherent and unavoidable.

Another interesting feature, which is very satisfactory to us, is the fact that under fault conditions on either system, the electric converter prevents excessive flow of current and actually limits it to slightly more than full load, as the authors bring out. A big interconnection which does not limit the flow of short-circuit current, as this does, imposes greater short-circuit duty on equipment and easily could cause much expense in circuit-breaker replacements in addition to affecting the stability of the systems. These electronic converters eliminate these two difficult and expensive problems.

Even for ties between systems of the same frequency, the degree of control of the power between the two systems obtained by electronic-frequency-converter installations would be a distinct advantage in some cases, although modern steam turbine governors and load controls make interconnections between systems very practical and have in

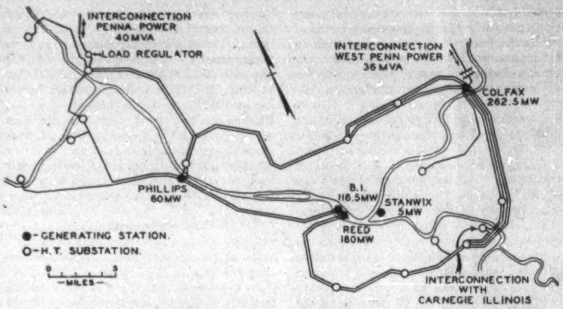

Figure 1. Duquesne Light Company system with 66-kv transmission and generating stations

For detail of Carnegie-Illinois 60- and 25-cycle systems see Figure 1 of paper 44-144

recent years minimized the degree of power swings over such connections. Even so, there might be installations requiring a smooth control of the flow over interconnections, and the engineers planning such interconnections should explore the possibilities and economic advantages of electronic frequency converters before arriving at their decision.

It is interesting to note the authors' statement, that the converter handles fault currents, internal and external, and restores power under normal conditions after an elapsed time of not over $^4/_{11}$-second after fault release, thus reducing fault damage, duty, and stresses on equipment and circuit breakers below extents created by corresponding size rotating equipment. This is of particular interest to the relay protection engineers and is more important to such engineers for a large power system. It is particularly interesting to engineers applying circuit breakers and to those concerned with the stability of the generators.

The Duquesne Light Company has been operating in parallel with adjacent power systems, the West Penn Power Company, and the Pennsylvania Power Company since about 1925, and such interconnections are still in satisfactory service. Several years ago we installed a regulating transformer in The Pennsylvania Power interconnection to permit the parallel operation of our system with both these interconnections simultaneously and to control the flow of power reasonably close (within steps of 2,000 kw more or less) over the tie with the Pennsylvania Power Company. In our case, each interconnection above one requires a regulating device to control the flow of power, so that it will not flow into our system from one interconnection and out over the other one, the regulating transformer being set to make the second interconnection feed into our system. Of course, rapid load swings cannot be controlled by this regulator.

We are making investigations and doing research jointly with the Steel Corporation and the General Electric Company to compare the 60-cycle voltage with the flicker in it caused by forced commutation failures with a standard 60-cycle laboratory wave to determine the magnitude of the fluctuating voltage. These staged tests were just finished late last week and have not been completely analyzed, but they appear to indicate that the voltage fluctuation at the connecting point between the 60-cycle systems of Duquesne Light and the Steel Corporation was in order of four to five rms volts for forced commutation failures. These deviations were observed on a cathode-ray oscillograph and photographic records were obtained. Varying reports were received from previously informed observers. Some could not even discern the flicker; most of them could but did not consider it objectionable, although the fluctuations were not nearly so prolonged during the test as they were when the units first were placed in service. The two tests were of very short duration (about five or six seconds each), and continued flicker of this magnitude would have been objectionable to many. Certainly it was during the first stages of operation of the sets. The test fluctuations occurred at the rate of 1.5 per second, which is approaching the most critical frequency for visual perception and objection.

I have a few questions to ask the authors:

1. I am interested in the over-all efficiency of the installation, and some reference is made to the efficiency of a typical electronic converter in Figure 10 of the paper. What I would like to know is this: Shall we consider the efficiency shown in Figure 10 as that of the Carnegie corporation installation, and if not, what is the Carnegie corporation efficiency?

2. Under the heading, "Control of Electronic Disturbances," reference is made to large currents in the a-c supply line during arc-backs, and elsewhere there is a statement to the effect that for short circuits on the receiving system the installation will permit only slightly more than full-load current to be drawn from the supplying system. Probably the high-speed relaying as covered in the same section of the paper covers this, but I believe a further statement might be helpful.

3. Suppose the load keeps increasing on the set for reasons other than faults, such as the tripping off of one of the large 25-cycle generators. Then, what load conditions exist on the converter set? Does it automatically shirk the increase and hold to its former load and thus force the other 25-cycle generators to pick up what has been dropped? Or does it just absorb the load? My reading of the article is that the electronic converter is so fast that it does not absorb the load.

4. The section of the paper, "General Applications for the Electronic Converter," raises the question as to just what kind of load—load which can or cannot produce its own reactive current? Just any industrial load? High-power-factor load? Low-power-factor load? If I understand rightly, these electric converters cannot pick up a "dead" system, as the system to be picked up must provide the reactive from some other source. I would like some enlightenment on this.

5. To a public utility man, the matter of repairs and spare equipment is very important, and I would like to ask Mr. Cramer about his company's experience on this: How many spare tubes on hand? How long to replace? and so forth.

F. W. Cramer (Carnegie-Illinois Steel Corporation Pittsburgh Pa.): Carnegie-Illinois Steel Corporation operates three large 25-cycle power systems; one in the Chicago district with a generating capacity of 297,800 kw, another in the Pittsburgh district with a generating capacity of 138,000 kw, and a third in the Youngstown district with a generating capacity of 47,200 kw. There is also a 60-cycle generation of 50,000 kw in Pittsburgh.

Since these systems supply steel-mill loads, a close control of voltage and frequency as practiced by utilities is not required, and variation in frequency from $22^1/_3$ to 26 cycles due to sudden load swings, or a drop in voltage of 10 per cent does not interfere with operations.

In the Chicago district, Gary Sheet and Tin Mill's wide-strip operation was installed in 1936 using 60-cycle equipment with a load demand of about 42,000 kw. The power is purchased from the public utility company. This 60-cycle load since has been increased as new equipment was placed in operation, and 60-cycle power gradually is replacing 25-cycle power in that area.

In 1940 studies were made on possible methods for interconnecting the 25-cycle system with its wide variations in frequency and voltage to the utility 60-cycle system with practically constant voltage and frequency conditions and have power feed either way.

These studies indicated that the mercury-arc rectifier and inverter combination, could be used, where frequency variations would be eliminated in the d-c tie, and a 6,667-kw unit was purchased. The war emergency handicapped the development of this project and delayed completion of the unit. However, it has been in operation for several months and is a very satisfactory addition to our facilities.

In the Pittsburgh district the Irvin plant, installed in 1938, together with the Edgar Thomson slabbing mill operation, were designed as 60-cycle plants and the power—about 45,000 kw—purchased from the public utility company.

In 1941, when the emergency expansion program was authorized, the electric furnaces at Duquesne works and the blast furnaces at Edgar Thomson were designed for 60-cycle power.

The additional blast furnaces at Edgar Thomson works produce enough gas for steam production over and above all other requirements to generate 50,000 kw, and, if the gas were not used for this purpose, it would be wasted. Two 25,000-kw 60-cycle turbogenerators were installed to utilize this available waste fuel. This, however, did not solve the power problem for the open-hearth slabbing mill, 160 inch plate mill, and forgeshop project located in the Homestead works, which project is located in a 25-cycle area near modern mills with 25-cycle main-drive motors. The topography of the country plus lack of space on mill property made a 60-cycle high-voltage line a costly project with complications of dual transmission systems. Studies on this problem indicated that the 50,000 kw produced by the 60-cycle turbines would about balance the additional electric load created by the three new projects, and, if a frequency converter could be installed, the 20,000-kw 25-cycle load in the new Homestead project could be served from those turbines.

As in the Chicago district, means for this frequency conversion were studied and propositions requested on both electronic and rotating equipment. The cost for an installed 20,000-kw electronic unit without foundations or building was $50,000 greater than for a rotating unit. However, the main unit for a Scherbius set weighed 715,000 pounds and for a Kramer set weighed 507,000 pounds plus two rotary converters at 76,600 pounds each. Either type of rotating unit would require a 50-ton crane to handle the heaviest piece. The total weight of the 48 units comprising the electronic converters was 135,000 pounds, and these could be installed without foundations on a floor designed for a load of 150 pounds per square foot. The location for this unit is in the flood area, so that the floor level for equipment must be 12 feet above ground level. This made the heavy foundations for rotating equipment more expensive.

When the total cost of the project, including building, foundations, crane, steel work, and ventilating system for the rotating equipment, was compared to the similar total cost for electronic equipment, there were considerable savings in favor of the electronic. There was also the benefit of having two complete units in the electronic project.

Delivery of the electronic equipment was promised in 427 days compared with 650 days for the rotating, and the quantity of critical materials involved was considerably less.

The efficiency of the electronic unit operating at a monthly load factor of 50 per cent should be about five per cent better than the rotating unit, which on 100,000,000 kilowatt-hours per year would mean a savings of 5,000,000 kilowatt-hours. This saving at 0.005 cent per kilowatt-hour equals $25,000 annually. In addition, no operator is required.

For the past two months there have been no outages of the equipment; the over-all efficiency from 69 kv 60 cycles to 44 kv 25 cycles has been 94.88 per cent.

As for operating costs, it will require several years to obtain an average value. A tube life of five years is expected. Other than tube replacements repair costs should be very minor.

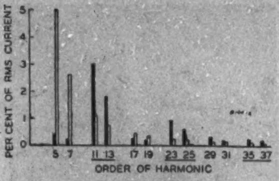

Figure 2. Harmonic current on 60-cycle supply side

Black columns—Measured with 12,500 kw on 12-phase unit

White columns—Calculated for six-phase rectifier

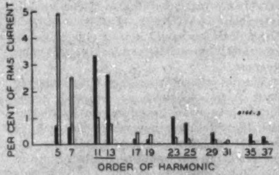

Figure 3. Harmonic current on 25-cycle load side

Black columns—Measured with 12,500 kw on 12-phase unit

White columns—Calculated for six-phase rectifier

H. E. Kent (Edison Electric Institute, New York, N. Y.) and **E. B. King** (American Telephone and Telegraph Company, New York, N. Y.): Wave shape of power-circuit current and voltage is one of the important factors in the inductive co-ordination of power and telephone circuits. Rectifiers as a source of wave-shape distortion have been discussed before the Institute in the past and are treated in various published engineering reports of the Joint Subcommittee on Development and Research of the Edison Electric Institute and the Bell Telephone System. However, a large majority of the available experimental data on rectifier wave shape is for rectifiers operating without grid control. Consequently, representatives of the Joint Subcommittee on Development and Research were especially glad to have an opportunity to co-operate in a study of the wave shape of the electronic frequency converter described in the paper by F. W. Cramer, L. W. Morton, and A. G. Darling. At the time the test was made only one of the 12-phase 10,000-kw units was in operation, transferring power from the 60-cycle to the 25-cycle system. The following results were observed with the unit carrying 125 per cent of its rated load.

The solid bars in Figure 2 of this discussion indicate the measured harmonic currents expressed in percentage of the rms line current on the 60-cycle supply side of the electronic converter. Also shown in the Figure 2 are calculated harmonic currents for a rectifier of the same size and of the same equivalent transformer impedance operating six-phase without grid control. A comparison of these two sets of values at the 11th and 13th, 23rd and 25th, and 35th and 37th harmonics (nonsuppressed in 12-phase operation) indicates the frequency converter harmonics to be about two or three times as large as those for the rectifier. This relation is in general agreement with observations in several other situations, namely, that a rectifier operating with substantial grid control gives rise to materially larger harmonics than one operating without such control.

Table I

	Current × TIF* (I·T)	Voltage TIF*	Voltage TIF,* Converter Off
69-kv 60-cycle side	21,000	39	18
44-kv 25-cycle side	11,000	54	9

* Telephone influence factor

The characteristic suppression of certain harmonics by the 12-phase arrangement is illustrated also in the figure. A high degree of effectiveness is indicated at the 5th and 7th harmonics. For the higher order of suppressed harmonics (17th and 19th, 29th and 31st, and so forth) the effectiveness decreases as the order of the harmonic increases.

Corresponding information for the same orders of harmonics on the 25-cycle load side is presented in Figure 3. The relationships exhibited here are very much the same as for the 60-cycle side.

Tests results from which these values are taken carried the analyses considerably higher in the frequency range. In general, the harmonics decreased in magnitude with increasing frequency. On the 60-cycle side the harmonics above the 37th (2,220 cycles) did not contribute materially to the over-all inductive influence. However, on the 25-cycle side the 37th harmonic (925 cycles) is about the middle of the band of frequencies important from a telephone noise standpoint. This is illustrated in Figure 4 showing for a wide range of harmonics the current times telephone influence factor product ($I \cdot T$), the usual index of the effect of power current on telephone noise induction. The relatively substantial magnitudes of some of the triple series harmonics (such as the 27th, 39th, and 45th) shown appeared to be caused by the operation of the inverter. Comparable values were not observed on the 60-cycle rectifier side of the unit, although there were found small but appreciable even harmonics, the largest of which was the 4th with a value of about one per cent. Such even and triple-series harmonics are not ordinarily present in the supply to nongrid controlled rectifiers.

The over-all influence factors of the power-system current and voltage, as measured at the electronic converter location are given in Table I of this discussion.

In this particular situation there was no telephone noise problem caused by the operation of the electronic frequency converter. There were no extensive exposures between telephone lines and the high voltage power lines, and the 60-cycle power-system arrangement was such that the increased

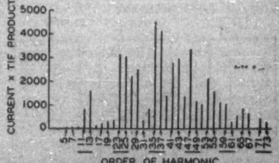

Figure 4. 25-cycle harmonic current times telephone-influence-factor products measured with 12,500 kw on 12-phase unit

wave-shape distortion did not reflect back appreciably to the lower-voltage distribution circuits.

It appears that the choking effect on the harmonics of the relatively high effective reactance of the frequency-converter transformers was a factor comparable in importance to the multiphase operation in minimizing wave-shape distortion. The transformer with an auxiliary reactor had about three or four times the reactance commonly encountered with ordinary rectifiers.

E. R. Whitehead and **V. E. Hill** (Duquesne Light Company, Pittsburgh, Pa.): As a supplement to B. M. Jones's remarks on paper 44-144 herewith is a brief description of the scheme employed to measure the flicker and some more detail on the reports of the observers.

Measurement of the transient voltage drop at the 66-kv bus of the interconnecting substation was effected by a null method utilizing a cathode-ray oscilloscope to compare this voltage with that furnished by a sine-wave generator at the meter standardizing laboratory of the Duquesne Light Company.

For convenience both voltages were brought over telephone lines to the system operator's office where the oscilloscope and high-resistance voltage dividers were connected as shown in Figure 5. The phase shift caused by the capacitances of the telephone lines was compensated by adjusting the stator position of the sine-wave generator, and the magnitude balance was obtained by means of the slide wire potential dividers. In this way, almost perfect fundamental frequency balance was achieved, leaving only the harmonic unbalance. The latter was between one half and three quarters of a volt. In order not to darken the film excessively with this residual trace while time-exposing the film to record the transients, a mask of approximately one-volt width was applied to the screen.

Figure 6 shows the record obtained during one of the staged multiple electronic faults. The scale marking is an over-all calibration made by depressing the voltage at the interconnecting substation in accurate two-volt steps. The maximum drop indicated is 4.9 volts and the minimum is 4.0 volts.

There were eight fluctuations in about five seconds, or about $1^{1}/_{3}$ dips per second. The frequency was obtained from an Esterline ammeter chart run at high speed, which is satisfactory for recording this data, but it is not accurate, of course, for the magnitude of the current swings.

Two such tests were performed, and the results from the previously informed observers are presented in this discussion. It is recognized, however, that tests of such short duration would not be objectionable, and they were satisfactory for this particular purpose.

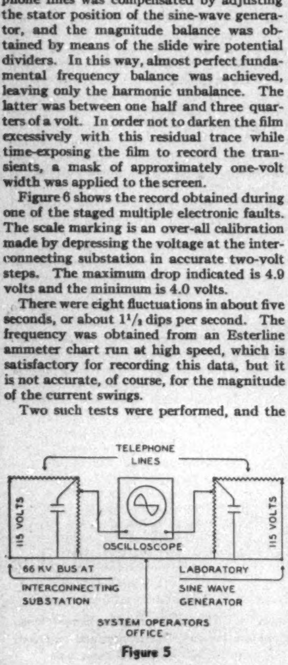

Figure 5

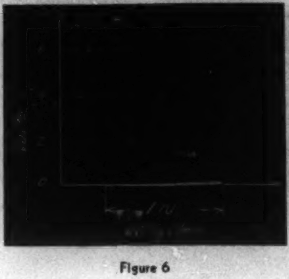

Figure 6

We did not get as many questionnaires back from observers as we had hoped for. There were no observers located at the Illinois substation where the voltage fluctuations were measured, all the observers being fed from other substations on the 66-kv ring. In all but one case this meant that observers were fed from the usual 4-kv distribution system supplied over 22-kv lines emanating from the 66/22-kv ring substations. The one exception is a 66/4-kv substation known as Brentwood. In the case of the ring substations nearest to Illinois, where it was calculated the dips amounted to approximately $3^{1}/_{2}$ volts, five out of nine observers detected the flicker on both tests. At another ring station still further away where the fluctuation was only $2^{1}/_{2}$ volts, only three out of 14 observers discerned the flicker in both tests. However, at the Brentwood station, which is between the two ring stations and had about three-volts fluctuation, seven out of nine noticed the flicker. We do not know just why the flicker is so much more noticeable from this type of station, but the complaints received when the sets were first placed in operation seemed to justify this test experience.

Some observers saw the flicker from one test but not the other, but these have not been included in the above results. Six observers on the other half of the ring, which is separated electrically from the half under observation by transformers and bus reactors at the generating stations, did not observe any flicker at all, as was expected.

It has been calculated that it takes approximately 50,000 kva to give a $4^{1}/_{2}$ volt dip at the Illinois substation bus.

We realize that these tests are not complete, but they were performed with the idea of trying out the scheme to see if it would work and then possibly making more exhaustive tests later. We did learn a considerable amount about testing procedure and would make the tests somewhat differently, if we decided to perform them again. The tests have demonstrated to our own satisfaction that the electronic converter can give rise to sufficient voltage flicker on the high-voltage lines of a large power system to cause perceptible flicker, which is objectionable if sustained. As Mr. Jones has said, these flickers were frequent enough to be objectionable only during the first few days after the sets were placed in operation; since then, even though we understand that there may be several dips a day, we have had no trouble or complaints from this source.

J. H. Cox (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The authors of the group of papers on electronic power conversion are to be highly congratulated on an excellent piece of engineering. It must be obvious to all that the converting of power from one frequency to another in large blocks by electronic devices in the present state of the art involves many difficult problems. Apparently all of these problems were solved in a manner which resulted in a satisfactorily operating unit. However, there is some danger that the very excellence of the engineering job done will give rise to over-optimism and the unjustified impression that the development is now finished, that the electronic frequency converter is now a fully developed device and can take its place alongside the mercury-arc rectifier for the familiar job of converting alternating current to direct current, ready to be installed widely the same as other familiar electrical apparatus. At the risk of being considered a pessimist, I definitely do not believe that this is the case. I share the opinion of most engineers working with electronics that as time goes on we are going to witness a tremendous further increase in the use of electronics in the power field, and the uses will include probably frequency changing and possibly d-c transmission, but the time for the general application to these special services has not yet arrived.

I would like to emphasize some of the more important attractions and limitations of the electronic frequency converter, many of these being treated only lightly in the papers just presented. The most valuable feature of the electronic frequency converter is the variable frequency ratio. This feature avoids the sometimes prohibitive procedure of tying the frequencies of two a-c power systems rigidly together. Less important attractions are extreme ease and flexibility of control and absence of noise and vibration. Against these attractions must be weighed the following disadvantages:

a. A present high initial cost. (The cost of upkeep can be estimated only when the life of the relatively expensive tubes is established.)
b. Adverse power factor and harmonics.
c. Low overload-current limits.
d. Complicated control and protection circuits.
e. Inability to supply isolated loads without a further considerable increase in complication.

An electronic frequency converter transmits only kilowatts and contributes nothing to the reactive kilovolt-ampere requirements of either power system. This would not be so serious, but the converter itself requires additional reactive kilovolt-amperes for its own operation, particularly on the inverter or receiving system, and this requirement is really relatively large. An indication of the magnitudes of this requirement is given in Figure 9 of the Cramer, Morton and Darling paper.

This requirement for reactive kilovolt-amperes is related to the more serious limitation in overload current. For a given setting of inverter operating angle there is a definite limit in the current that can be supplied by the inverter without failure, even though the tubes perform perfectly. Unfortunately the higher the setting for overload current the greater becomes the required reactive kilovolt-amperes during continuous normal operation. Also, variations in the voltage of the inverter or receiving system have a profound influence on this overload current limit. As described in the papers, the converter will not supply an isolated load. This can be accomplished by a considerable increase in apparatus and complication, or by a considerably different basic circuit.

There is a minor point in the papers I feel deserves criticism. The word "shoot through" to indicate a failure of commutation is a rather unscientific term to be used in engineering work.

In closing, I want to say that I am very happy to see the great amount of work being done on all phases of electronic power conversion and to be participating in that work. It is certain to bear fruit. Usually it is more orderly and safer to do the work in the laboratories, until satisfactory and economic operation is assured. If apparatus is installed in commercial service prematurely and unsatisfactory performance results, the apparatus suffers for a long time from an initial bad reputation. I trust that the splendid engineering job done on the Carnegie-Illinois installation will avoid a bad reputation for the electronic frequency converter.

C. W. Frick (General Electric Company, Schenectady, N. Y.): The effect of the equipment on the wave shape of the power system mentioned in paper 44-144 in several places, is treated in considerable detail in the discussion presented by Mr. Kent and Mr. King. That discussion makes available the results of co-operative tests made especially for the purpose of obtaining data pertinent to wave shape. In general it substantiates the conclusion that the harmonics and wave-shape distortion for rectifiers and inverters depend upon the same factors. This equipment differs from an ordinary rectifier installation in two respects:

(*a*). The percentage reactance of the transformer is much higher.

(*b*). Phase control is used to a greater extent than usual.

The tests reported by Mr. Kent and Mr. King were made with 12-phase operation only because but one of the two units in this station was available at that time. The paper states that the complete installation of two units operates 24 phase. This eliminates or reduces the 11th, 13th, 35th, 37th, and other harmonics associated with odd multiples of 12. Figures 2 and 3 of the discussion mentioned show harmonic currents for the 12-phase unit together with calculated values, which evidently allow for the transformer reactance but do not include phase control. The increase of some two or three to one due to phase control seems reasonable. Oscillograms similar to the current waves which were analyzed are shown in Figure 6 of the paper, curve *b* for 25 cycles and curve *d* for 60 cycles.

Table II

	Current Times Telephone Influence Factor		Voltage Telephone Influence Factor	
	Estimated	Measured	Estimated	Measured
60-cycle side	25,000	21,000	33	39
25-cycle side	10,000	11,000	48	54

A short-cut method for estimating voltage and current telephone influence factor associated with ordinary rectifiers is described in reference 3 of the paper. Applying this method and making further allowances for transformer reactance and phase control as already mentioned, the estimates given in Table II were obtained, which are compared with the measurements reported by Mr. Kent and Mr. King.

The agreement is reasonably good. It is too early, however, to establish any general factor for this purpose.

The complete installation carries twice the load of the unit tested but operates 24 phase. Therefore, the telephone influence factor does not increase and is actually reduced considerably. Harmonic analyses and telephone influence factor measurements were not made for 24-phase operation, but the improvement in voltage and current wave shape is shown by the oscillograms of curves *E* and *F* in Figure 6 of the paper.

H. Winograd (Allis-Chalmers Manufacturing Company, Milwaukee, Wis.): I wish to congratulate the authors participating in the symposium on their accomplishment in this development work. It is evident from the papers that much brain power has been expended on this job, and the papers are a valuable contribution to the electronics literature.

A 6,700-kw electronic frequency changer consisting of two parallel sections was supplied by Allis-Chalmers for the Gary, Ind. plant of the Carnegie-Illinois Steel Corporation, to provide a flexible tie for the transfer of power between the 25-cycle and 60-cycle systems. The complete unit is in operation, and one section has been in service since the fall of 1943.

The introduction of the electronic frequency changer in the United States is an important milestone in the application of electronic power equipment and in the field of power transmission. The Carnegie-Illinois Steel Corporation and the engineers of that company deserve great credit for pioneering these installations.

The frequency changer in the Gary plant differs from that described in the papers under discussion in regard to the type of converting equipment used, the operating voltages, and the switching and control circuits. One essential difference is that the Gary rectifiers have a continuous excitation arc maintained at the cathode. This resulted in a considerably simpler cathode excitation and grid-control system as compared to that of the pentode ignitron. A pentode tube with a continuous excitation arc would have only one auxiliary electrode at the cathode instead of five. Such a design would require other internal changes, but an over-all simplification would undoubtedly result.

Another advantage of using a tube with a continuous excitation arc would be the elimination of shoot-throughs resulting from misfires. Although such disturbances are taken care of by arc suppression in the rectifiers without tripping of circuit breakers, they are objectionable, nevertheless, if they occur frequently. According to the oscillogram in Figure 14 of Dr. Willis' paper, the current surges in the supply circuit reach values of about three times normal, and this is followed by a number of cycles of zero load.

These remarks should not be considered as criticism of the designers, who have done a splendid job in making the equipment perform successfully. Time, operating experience, and further development will determine which type of converting equipment is best suited for this service.

I should like to ask the authors several questions in regard to the operation of the equipment:

1. What is the function of the reactor in the d-c circuit? Is it used for suppressing the harmonic currents, or is it intended also to reduce the rate of current rise in case of a fault?

2. In Figure 14 of Dr. Willis' paper, why does the bottom trace of the 60-cycle oscillogram indicate zero up to the time of the fault, and why does it have a decremental shape after the fault?

3. If the arc in a tube is sustained for several cycles during a disturbance, is it possible for the arc to jump to the tank, and what would be the effect?

4. On the average how frequently do shoot-throughs due to misfires occur on the frequency changer?

W. E. Gutzwiller (Allis-Chalmers Manufacturing Company, Milwaukee, Wis.): The five papers presented give an interesting account of the equipment, the installation, and the early commercial operation of this new electronic frequency changer. They not only stress the advantages of this type of converter, but also give its limitations in comparison with the older rotating type converters.

Referring in particular to the paper of Alexanderson and Phillipi, I believe it is in order for completeness' sake to say that other manufacturers of power electronic equipment in this country and abroad also have contributed to the development of this electronic converter.

A 25/60-cycle electronic frequency changer of $^{3}/_{8}$ the unit capacity of the one described in this paper built by Allis-Chalmers Manufacturing Company, was put in commercial service in September 1943 in one of the Midwestern mills of Carnegie-Illinois Steel Corporation and has been used for power demand control ever since. This unit, though performing a very similar service to the one in the Pittsburgh area, uses a somewhat different type of equipment and circuits. It also has, however, a d-c link between two separate electronic units and is provided for power conversion in both directions.

In Europe mercury-pool-type electronic power inverters in various forms have been used extensively since early in 1934. In that year I made a trip through Italy, Switzerland, and Germany for the specific purpose of studying these installations and obtaining first-hand information on the equipment, on its protective and control devices, and its operating performance.

Among several 3,000-volt electronic power converters installed on the Italian State Railways and designed for rectification and inversion for the purpose of supplying the railroad with d-c power and of absorbing regenerated power from locomotives, one installation was particularly noteworthy as it represented, according to my records, the first commercial high-power electronic frequency changer. It consisted of a railway substation with three 2,000-kw 3,000-volt d-c rectifier–inverter units rated for heavy duty railway service. A-c power was furnished from two separate systems, one having a frequency of 42, the other of 50 cycles. Both systems could supply jointly or separately the entire railway load. However, it was desirable at times to feed surplus power from the 50-cycle into the 42-cycle system through the 3,000-volt d-c railway bus, under which condition the equipment operated as a frequency changer.

In 1935 six 1,500-kw 3,000-volt rectifier–inverters were installed on a mountainous section of the South African Railways, where heavy regeneration from freight trains had to be absorbed. Several additional duplicate units were ordered two years later. All these installations have now, according to information available, been in successful operation for some ten years.

In 1936 the first electronic frequency changer rated 3,000 kw supplying a $16^{2}/_{3}$-cycle single-phase electric railway system from a three-phase 50-cycle utility power system was placed in service. This unit, in contrast to previously mentioned installations, did not make use of a d-c link but converted the power in one electronic unit. Power conversion was accomplished in both directions and the unit could take care of its own reactive kilovolt-ampere requirements as well as of those of the single-phase railway system.

A paper was presented before the AIEE by O. K. Marti, describing this type of frequency converter.[1]

In 1939 another electronic frequency changer of 1,600-kw capacity was installed in Switzerland and formed a flexible two-way tie between a 40-cycle three-phase railway power system and a 50-cycle municipal system. Here again no d-c link was used and conversion was accomplished in a single electronic unit.

Worth mentioning in connection with the application of electronic power converters for future high-voltage d-c transmission is an experimental installation made in Switzerland in 1939. It consisted of a 15-mile d-c transmission over an existing three-phase line at 50,000-volt direct current and 500-kw capacity. It used, at times, only one line conductor with ground return. The rectification and inversion was accomplished on only one electronic unit at each line terminal. This installation probably represented the highest voltage obtained in a single electronic valve of the mercury-pool type.

In conclusion, I would like to say that with two large-scale installations already made in this country, this new electronic power-conversion tool now definitely has left the laboratory stage and should find many new uses in the near future. One of the most interesting ones undoubtedly will be its use in connection with long-distance transmission of high-voltage d-c power.

The management and the engineers of the Carnegie-Illinois Steel Corporation, who have made these two pioneering installations possible, deserve a great deal of credit, and their co-operation and pioneering spirit is being appreciated fully by the manufacturing and power industry.

REFERENCE

1. NETWORK COUPLING BY MEANS OF STATIC ELECTRONIC FREQUENCY CHANGERS, Othmar K. Marti. AIEE TRANSACTIONS, volume 59, 1940, September section, pages 495–502.

E. F. W. Alexanderson, C. H. Willis, W. N. Gittings, H. C. Steiner, and A. G. Darling: The discussions presented have, in general, contributed either additional information or given valuable and constructive criticism, so enhancing the value of the material presented in the papers.

F. W. Cramer, to whom much credit is due for his foresight of desirable characteristics and his confidence in the abilities of the manufacturers to meet requirements, has added much valuable background information leading to the selection of the electronic converter and contributed interesting results of operation.

Mr. Kent's and Mr. King's discussion of wave shape has been ably reviewed by C. W. Frick.

B. M. Jones's presentation of the effects of the electronic converter upon the operation of the Duquesne Light Company shows how little the operation of the converter can be noticed, after the period of initial adjustment. It can be assumed that the reactive kilovolt-amperes required by the converter are supplied by the Steel Company.

Replies to Mr. Jones's specific questions are as follows:

1. The efficiency of the Carnegie converter is illustrated in Figure 10 of the Cramer, Morton, and Darling paper. That these data are realized in practice is substantiated by Mr. Cramer's statement that 94.88 per cent efficiency has been attained under normal running condition.

2. The current drawn from a supply system of zero impedance during an arc-back in the rectifier is a function of the impedance of the transformer and rectifier circuit. In this instance this current is in the order of five to six times the kilovolt-ampere rating of the rectifier transformer.

Mr. Jones then mentions that "elsewhere in the paper there is a statement to the effect that for short circuits on the receiving system, the installation will permit only slightly more than full-load current to be drawn from the supply system and suggests a more complete statement would be helpful.

Apparently, Mr. Jones referred to clause 6 "Fault Control" under the general heading of "Distinguishing Characteristics." This subject is treated in more detail later in the paper under the heading, "Effect of External Faults." The duration of fault currents is discussed under the preceding heading of "Control of Electronic Disturbances."

3. This question has to do with a control of load through the converter with conditions under which prime movers have to increase or reduce their loads. Most prime movers maintain a relatively constant frequency and in so doing increase or reduce their output. The electronic converter inherently transmits power not as a function of frequency but as a function of change in ratio of input to output voltages. Unless this ratio changes during a fixed load setting of the control, there will be no change in the output of the converter. Changes in loads of a system will be noticed mostly in frequency changes and to a less degree in voltage changes. Therefore, unless the voltage ratio of the converter changes because of load changes, there will be no tendency for the converter output to change. The watt regulator of the converter acts to restore the load through the converter as changed by voltage-ratio changes.

An inspection of the recording meter charts of Figure 13 of the paper will show that the converter does not respond to frequency variations and maintains practically constant output with system voltages varying.

The constant-load type of control was specified to be used for this installation. The paper points out that other forms of control are applicable, one of which would make the converter share load proportionally with other prime movers.

4. Further explanation of the types of independent loads that the converter can carry is requested in this question.

The general types of electronic converters described in the paper, providing flexible frequency ratio ties, have characteristics such that reactive kilovolt-amperes must be furnished to the converter when it is in parallel with synchronous generation or when it is supplying an isolated load. The reactive kilovolt-amperes of the load also must be supplied by suitable means. Voltage regulation of a converter supplying an isolated load rests solely upon a condenser, which must be available for this purpose. When these conditions are satisfied, stub-end feed from an electronic converter seems entirely feasible.

The foregoing statement answers Mr. Jones's questions as to character of loads—there are no restrictions provided the condenser is of appropriate size. The converter cannot, of itself, pick up a "dead load." It depends on the condenser to supply the initial action.

5. Mr. Cramer replied to question 5 about as follows:

In regard to repairs and spare equipment, the electronic frequency changer has been in operation too short a time for us to have obtained any reliable data. Outside of the tubes, which have an expected life of five years, there should be very little equipment which will require repairs or replacement. Six spare tubes are carried at the station. It requires about an hour to remove the unit from the line, change a tube, and replace the unit on the line.

The tests reported by Mr. Whitehead and Mr. Hill necessitated that the converter be forced intentionally into electronic fault conditions. The eight voltage fluctuations which occurred in five seconds were due to the operator's wish to be certain that the faults were sufficient to be observed. Normally, only one fault occurs which is cleared by the fault-suppression control. Electronic faults evidently are not a cause of noticeable system-voltage disturbance, and single faults do not cause customer reaction.

The authors are to be complimented on the ingenious and accurate method used in making these interesting tests which contribute valuable information to the project.

Mr. Cox summarizes, very well, the characteristics of the converter brought out in the papers which he terms disadvantages. Some further comment is needed to assign value to these limitations:

(*a*) and (*b*). Mr. Cramer has, in his discussion, given data which pertains to these points.

It has been pointed out in the papers that the reactive current required by the converter represents a charge against this type of unit. Once this is recognized and evaluated, it becomes a tangible factor and can be equated against advantages.

The question of adverse harmonics has been answered ably by Mr. King and Mr. Kent insofar as telephone interference is concerned. There is no noticeable interference. The effect of the irregular wave shape on rotating machines would be a difficult point to determine. Suffice it to say there have been no cases evidencing such effects to date. The effect of irregular wave shape in isolated-load cases might be analyzed more readily and provision made for compensation, if any were required.

(*c*). That "low overload-current limits" is a disadvantage would be true, if the objective were to try to sustain system voltage under fault conditions. Low-overload currents, on the other hand, reduce short-circuit duties and consequent destruction of equipment.

(*d*). It is true that the control and protection of the electronic converter presents problems unusual to the designer familiar with rotating machine control. However, these problems were handled by men whose previous experience had been with conventional rotating machines and transformers.

(*e*). Mr. Cox charges the electronic converter with an inability to supply isolated loads without a further increase in complication. Experience in other instances, cited by Mr. Gutzwiller and tried out in experimental form by members of the authors organization, show that the additions needed are largely in the form of conventional synchronous condensers and frequency-sensitive relays.

The points brought out by Mr. Cox as limitations therefore do not represent unsolved problems. What is known to be can be met with confidence.

Certainly no one can object to laboratory experiments as a road to gaining confidence for commercial installations. Field experience, backed by laboratory tests, and involving all elements is in the end the ultimate proof.

Mr. Winograd's description of the Gary electronic frequency changer and his comments regarding the difference between the continuously excited electronic tube employed there and the cycle-by-cycle-excited pentode ignitron are interesting. Either method of excitation could be employed with the appropriate design change in the tubes and circuits. Some provision must be made in the continuously excited unit in case the arc goes out, and there is some question as to whether the tube simplification due to the use of an ignitor does not overcome any increased circuit complexity. Also, as Mr. Winograd points out, there are other causes of inverter shoot-throughs than ignitor misfires.

In regard to Mr. Winograd's specific questions:

1. The function of the reactor in the d-c circuit is to keep the reactifier current continuous at light loads. It also tends to limit the rate of rise of current in case of fault.

2. The bottom trace in Figure 14 in Dr. Willis's paper represents the rate of current change in the d-c link. That is, it is the direct current as seen through an ordinary a-c transformer. It is therefore zero as long as the direct current is constant. The decremental shape is caused by the current decay through the d-c reactor and by possible saturation of the current transformer.

3. The tubes are provided with an arc-retaining ring in the cathode, which tends to prevent the cathode spot from wandering when direct current passes through the tube. Tests indicate that the ring is effective over a period of seconds and for fairly high currents. If the arc were to leave the cathode pool and anchor on the wall, it would, of course, burn a hole through the wall in time. For a period of several cycles it is improbable that anything would happen other than some slight vaporization of the wall metal.

4. Shoot-throughs may be due to a number of causes other than ignitor misfires. In the units described there are fault indicators which tally and differentiate between shoot-throughs and arc-backs. On the average and from all causes there have been between two and three shoot-throughs per day at full-load conditions. These are decreasing in frequency as minor circuit adjustments are being made. Considering one unit with power flow to the 25-cycle side, there are nearly 26,000,000 ignitor operations for the 12 tubes in a 24-hour period. Assuming that all of the faults are caused by ignitor misfire, the percentage of failure is very low. With such operation there is every reason to believe that ignitor misfire can be reduced to zero.

Totalizing Contents of Aircraft Fuel Tanks

Discussion and author's closure of paper 44-148 by J. R. Macintyre, presented at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944, and published in AIEE TRANSACTIONS, 1944, September section, pages 663–5.

G. F. Tate (nonmember; Wright Field, Dayton, Ohio): The increase in size and range of aircraft and the change in installation and operation requirements have accentuated the need for more accurate and reliable fuel quantity gages which are not affected appreciably by reasonable changes in the attitude of the aircraft under flight conditions.

The totalizer system described is a good step in this direction. By using a multiplicity of units the indications of which are averaged together or "totalized," it is possible to obtain a more accurate over-all measure of the fuel available. The location of the units are not limited to the lateral plane only, as shown in Figure 1, but can be used in the longitudinal or wing chord plane as well.

Units similar to those described now are being installed in large aircraft to correct difficulties experienced with an earlier "step-type" fuel-gage system initially installed on this aircraft. Totalizers also have been proposed for several new aircraft now being constructed.

Present designs of float-operated totalizer systems are limited to the use of indicators with 90 degrees to 110 degrees of arc-pointer movement. The resulting short scale lengths are not ideal, especially for measurement of large quantities of fuel.

Effort should be directed toward developing and refining the totalizer method to provide instruments with concentric pointers which can be read through 300 degrees of arc or more.

J. R. Macintyre: Mr. Tate's comments are most welcome. He is in an excellent position to understand aircraft instrument requirements.

Some work has been done on a moving-magnet-type ratio meter having a scale length of 300 degrees. With suitable demand for these instruments their development can be completed, and, if the same methods described in the paper are applied, a totalizing indicator having the long-scale feature can be produced.

Design Considerations in Aircraft Instruments to Meet War Service

Discussion and authors' closure of paper 44-150 by C. F. Savage and J. M. Whittenton, presented at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944, and published in AIEE TRANSACTIONS, 1944, pages 992–8.

John G. P. Callahan (nonmember; captain, United States Army Air Corps, Wright Field, Dayton, Ohio): This paper is an excellent compilation of many of the detail considerations which have entered into the design of aircraft instruments for the Army Air Forces. The general design problem may be stated in two parts:

(*a*.) The instruments should provide satisfactory indications on any military aircraft operating in and over any part of the world from several hundred feet below sea level to 50,000 feet above sea level.

(*b*.) The instruments and their packages should permit satisfactory shipment by any available means—coolie, truck, rail, water, and air—and satisfactory storage for at least eight months in any part of the world.

Most of the aircraft instruments have answered the problem in a manner which is reflecting great credit on the label, "Made in U.S.A."

The following elements of the problem require continued development:

(*a*.) Vibration insulation methods and materials for use in aircraft installations.

(*b*.) Satisfactory substitutes or additional sources of supply for sapphire and ruby ring jewels.

(*c*.) Lubricants and lubrication methods which will permit instruments to operate for at least one year of normal service.

(*d*.) Fungus growth prevention on and within aircraft instruments.

(*e*.) Prevention of rusting and corrosion of internal parts.

(*f*.) Simplified test equipment for first, second, and third echelons of maintenance.

(*g*.) Design of electric instruments so that they will be dust-tight and yet not fog on the interior of the cover glass.

Much good work has been accomplished to make aircraft instruments perform satisfactorily under combat conditions in this global war, but more work must be done. We must all continue to try harder.

J. T. Gove (Los Angeles, Calif.): The statement in this paper that "The urgent need for remote compass indication in heavily armored fighter airplanes, where direct-reading compasses are of little value, is satisfied by this really significant contribution to the art of instrumentation" should have been qualified. The conditions which resulted in unsatisfactory operation of direct-reading compasses, namely the addition of armor plate and equipment, are catching up rapidly with the remote-indicating compass. To obtain a satisfactory location for the remote-compass transmitter is an ever increasing problem with the aircraft manufacturer. It appears at the present time that the only relatively safe place for the remote-compass transmitter is in the wing near the tip where there is a minimum of equipment. The problem of finding a location with the minimum of magnetic disturbances may not be as great in large aircraft as it is in the smaller fighter and dive-bomber types in which practically all the available space is occupied by fixed equipment, such as radio, electric wiring and control cables, and removable equipment such as oxygen bottles, guns, and bombs.

It should be noted that in many military aircraft using the remote-indicating compass, the direct-reading panel compass also is installed. Some pilots have more faith in the direct-reading compass, even though it may possess greater errors than the remote-indicating compass. In combat the wiring to the remote compass is just as vulnerable as any other wiring installed in the aircraft. Damage to the remote-indicating compass wiring necessarily would render the system useless, but it would not in any way affect the direct-reading compass.

Exception is taken to the following statement in this paper under "Standardization of Mechanical Features":

> "Except for such an instrument as the flap and landing-gear indicator, to which there are numerous connections, there are very few cases where the standard screw-type terminals would not suffice and would still retain the desirable feature of interchangeability. The difficulties with standard Army–Navy electric connectors have been so numerous (corrosion being one of the worst offenders) that the present tendency is to avoid their use wherever possible."

C. F. Savage and **J. M. Whittenton:** The discussion by Captain Callahan of this paper is just another indication that the design of aircraft instruments is not static. The importance of continued work on designs for satisfactory operation over a wide range of altitudes, from several hundred feet below sea level to 50,000 feet above sea level, cannot be overlooked.

Recent operation of aircraft under world-wide climatic conditions has increased the importance of correctly packaging spare instruments for shipment such that they would not be affected by high temperature and high humidity, or by shocks and vibration due to all types of conveyance. Instruments will soon be packaged in hermetically sealed metal containers which will go a long way toward meeting the high temperature–high humidity problem, and scientific study is well under way to determine adequate packaging such that instruments will not be damaged by any type of transportation.

It is gratifying to know that the efforts of the manufacturers have met with the approval of such an authority as Captain Callahan.

The comments further the thoughts of the authors that the paper, as presented, was an interim report on the status of an art which is not static. It is recognized that continued development of design is necessary in order to meet the demands of the users. This will be done. It is agreed that adequate vibration-insulation methods must be devised and that adequate shock resistant materials must be provided in order to mount adequately aircraft instruments for long-time service under adverse shock and vibration conditions.

Much work has been done on substitute bearings for sapphire and ruby ring jewels. Thousands of aircraft instruments are being made on a production basis today having glass ring jewels in place of sapphire and ruby ring jewels. Indications are that their performance is satisfactory.

The developments in lubricants as a result of the war effort really have been significant. It is the opinion of the authors that some of these new materials will enable the objective as set forth by Captain Callahan to be reached for many instruments.

Tropicalization of aircraft instruments is receiving more attention from the instrument designer as additional information is received from field operations.

The importance of properly protecting all metal parts; the use of proper varnishes on coils; the elimination of certain materials; and the proper treatment of others to prevent fungus growth, rust, and corrosion are being studied. Large-scale work on this most important problem is being done, and test results thus far have shown great promise.

The importance of providing dust-tight instruments which will not fog from rapid changes in altitude is not being overlooked, and this will provide a very significant change in instrument design in the future. The ways of accomplishing this end are now in sight. Hermetically sealed instruments would assist in overcoming the difficulties now experienced in jeweling, fungus growth, corrosion, rust, and fogging by preventing oxidization, entrance of fungus spores, moisture, and other deleterious elements.

The authors feel that the experience which has been gained by the use of aircraft instruments during the present world conflict will aid materially in the making of better designs.

The authors have noted the comments by J. T. Gove regarding remote-indicating compasses and methods of connecting instruments as given in this discussion.

It is recognized that direct-reading compasses have been made and used for many hundreds of years, and it should be pointed out that the remote-indicating compass described in the paper is in its infancy, both from the standpoint of its design and its use. It has served a very useful purpose in spite of any limitations which can be pointed out. Each airplane has its own installation problem, and it is hoped that, as knowledge is gained in both the design and application of this new tool, any limitations which are imposed will be minimized. Both the remote-indicating compass and the direct-reading compass have their place.

It is agreed that the statement regarding difficulties which have been found with Army–Navy electric connectors should have been clarified, since the standard screw-type terminals are used on the class of instruments of the ammeter–voltmeter types.

A Unique Moving-Magnet Ratio Instrument

Discussion and author's closure of paper 44-152 by F. R. Sias and D. B. Fisk, presented at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944, and published in AIEE TRANSACTIONS, 1944, September section, pages 634–6.

A. W. Nash (El Segundo, Calif.): This paper goes rather extensively into the design and construction of a new type of instrument proposed for temperature indication in aircraft. No information is given as to whether such instruments have been used in aircraft service.

The substitution of a permanent magnet for the armature and attaching coil springs increase the simplicity and should decrease service maintenance problems. Some question arises as to the probability of increased bearing troubles as a result of the increased mass of the armature.

Probably the report would be more valuable if it included some of the experiences gained with other similar types of instruments which indicated the desirability of the development of this moving-magnet type of instrument. If the instrument has been used in aircraft service, a paragraph might be added to indicate the degree of success obtained.

C. W. Muller (nonmember; Wright Field, Dayton, Ohio): This is a very good paper on this indicator. When these indicators were first made, the scales were photographed individually and so were not interchangeable. The General Electric Company reports that now the dials are alike on all dual indicators, *AN*5795-6, and 85 per cent of the dials are alike on the single indicators, *AN*5790-6.

The Army Air Forces desires scales which are interchangeable in order to simplify instrument overhaul and stocking of parts.

The use of adjustable rheostats to obtain scale length and proper location of the center scale point is particularly advantageous from a maintenance standpoint, since it allows the stocking of fixed-resistance spools for the instrument. In addition, the instrument repairman does not have to wind spools to a definite resistance to obtain the correct calibration. This operation, heretofore, has consumed a considerable part of the total time for overhaul of an instrument of this type and required the use of additional decade resistance boxes.

The assembling of the coils and rotor into one compact unit introduces a maintenance problem. Would it be more efficient and economical to replace the complete unit rather than supply a customer separately with all the spare parts that comprise the coils and rotor unit?

D. B. Fisk: The authors are indebted to Mr. Muller for his discussion of the paper as well as for practical suggestions offered during the development of the device. In connection with the question on spare parts: Complete elements or the component parts are available as desired. Complete elements can be interchanged speedily by persons not possessing the skill required for replacing element components, and, for this reason, in the field it might be more economical to replace whole elements. In the factory or a place similarly staffed with trained personnel the substitution of components probably would be more economical.

The authors appreciate the comments and the questions raised by Mr. Nash. Thousands of these instruments have been furnished for aircraft service with gratifying results. The mass of the moving system amounts to about 0.7 gram, which is comparable to other movements using vee jewel and pivot bearings. Severe vibration tests have demonstrated the adequacy of this bearing system. The high value of torque to weight has been stated in the paper.

Recent Advances in Aircraft Tachometer Design

Discussion of paper 44-153 by R. G. Ballard and C. P. Hall, presented at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944, and published in AIEE TRANSACTIONS, 1944, September section, pages 646–8.

R. L. Williams (nonmember; Wright Field, Dayton, Ohio): This paper describes very well the improvements recently incorporated in aircraft tachometers.

The improvements in the tachometer generator, aside from the increased range of operating temperature which involved a lubrication problem, have been the direct result of service difficulties encountered since the advent of higher performance aircraft.

Although improvements in the temperature compensation of the indicator have been made, an examination of the compensated curve in Figure 6 will show that there is still need for improvement in the neighborhood of —30 degrees centigrade. Further improvement will depend upon the commercial availability of a better magnetic compensator material.

The magnetic-drag-type tachometer has proved satisfactory in the majority of installations due to its accuracy and installation flexibility and with the advances in design already mentioned, should provide a highly satisfactory aircraft tachometer system. Its chief disadvantage is its weight, which exceeds that of other tachometer systems which have been used. Consideration should be given to reducing the weight of the tachometer generator, which at present exceeds 2.5 pounds.

A New Frequency Relay for Power-System Applications

Discussion and authors' closure of paper 44-172 by H. J. Carlin and J. L. Blackburn, presented at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944, and published in AIEE TRANSACTIONS, 1944, August section, pages 553–7.

George Steeb (Buffalo, Niagara, and Eastern Power Corporation, Buffalo, N. Y.): An underfrequency relay with the proper characteristic will provide a new tool to protect power systems. The idea of using these relays as the authors suggest may be repelling to some people, but unquestionably there are some merits which ought to be considered.

Of course, the power companies may be accused of showing favoritism by dropping certain loads during a disturbance, but, when it becomes a choice between losing all the load and the sacrifice of a part of the load, the answer ought to be obvious.

In the paper Figures 5, 6, and 7 indicate that the relay has favorable characteristics. It would have been helpful to know how fast and at what frequency the contact of the relay reopens, after having been set at a predetermined value to close the contact.

The part that is not entirely clear from the paper is whether the relay would close its contact by the loss of potential. If this were so, load would be interrupted unless an instantaneous undervoltage relay is provided, which would operate when the voltage drops to a certain value and thereby open the trip circuit of the underfrequency relay.

Could the authors suggest a practical way of testing these relays in the field?

J. T. Logan (Georgia Power Company, Atlanta, Ga.): The authors have described the development of a device that might be applied as an ultrasensitive frequency relay. They describe applications for relieving overloaded generating plants by disconnecting loads at predetermined frequencies.

It has been my experience that load segregation on the basis of an assigned frequency value without regard to rate of frequency change is a dangerous practice. It ignores the recovery properties of generators either lightly loaded or floating unloaded for protection only, particularly in hydroelectric units whose gate responses are inherently sluggish.

We recently had an experience in Georgia where the practice described in Figure 2 of this paper would have been very bad. The system setup was approximately as follows:

System load	520,000 kw
System generation	380,000 kw
Received from other companies	140,000 kw
Spinning reserve capacity	100,000 kw
System shortage in generating capacity	40,000 kw

Under this setup the external power source not only failed completely but left 20,000 kw of its load connected to our system. The picture within a second or two became:

Generating capacity deficit	160,000 kw
System frequency	58 cycles
Generation increase (spinning reserve)	100,000 kw
Net generating capacity deficit	60,000 kw
System frequency	59.2 cycles

The external power source isolated a generating plant, synchronized it to the Georgia Power system, and normal frequency was restored. The above disturbance lasted 18 minutes, but no load was interrupted.

Had the authors' proposed relay system been in service on our system during this disturbance it would have interrupted perhaps the entire 160,000-kw load, if the underfrequency relays had been applied for 100 per cent protection.

Such problems in Georgia are handled by means of inertia-type underspeed relays which, except for very low voltage, respond only to the rate of frequency change. There are 15 such relays on our system. Only one of these relays operated, and it did so properly because of an out-of-step condition caused by a relatively weak tie between the particular generating plant and the main power system.

The application shown by Figure 1 of the paper would be very limited on our system, because there would be other customers supplied from the power system interconnection which should be receiving the benefits from high-speed circuit reclosure. I seriously doubt that any flat frequency responsive device would be fast enough for applications where high-speed circuit reclosure is involved without having its contacts set so close together that they would be making contact repeatedly for the slightest system disturbance. This being true, then the reliability of the scheme would depend upon the watt-relay interlock, thus eliminating the need for the underfrequency relay.

A. J. McConnell (General Electric Company, Schenectady, N. Y.): The frequency relay, though a necessary member of the relay family, has been one of the least used and until now, has received little attention in the technical press. For this reason it is a little-known fact that a frequency relay which uses the same principle as that of the subject paper was designed and produced in 1941. This discusser, however, takes no credit for the conception of this principle, since it is rather old. The

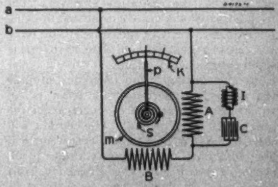

Figure 1. Reproduction from United States patent 649,007

disclosure was made by Charles P. Steinmetz in 1900 in a United States patent.[1] Figure 1 is reproduced from this patent together with the following quotation:

> "The relative values of resistance, inductance, and capacity are so chosen that their effects so far neutralize each other at some predetermined frequency as to render the circuit shunted about the coil *A* of the same time constant as coil *A* itself. Under this condition currents in coils *A* and *B* are in the same phase with each other, and no effect, therefore, is produced upon the short-circuited member *m* within their influence."

REFERENCE

1. United States patent 649,007, C. P. Steinmetz, May 8, 1900.

E. E. George (Southwest Power Pool, Little Rock, Ark.): It would be helpful, if the authors will tell us how this relay responds to rate of frequency change. It would seem that the induction-timing characteristic of this new relay would be largely responsive to the rate of frequency change. This would provide a valuable operating characteristic. For special applications, the inertia type of relay (developed by J. T. Logan of the Georgia Power Company) has been very successful, because it is responsive almost entirely to the rate of frequency change.

The timing curves presented by the authors are based on instantaneous changes in frequency. Such curves would be more useful to the operating engineer if based on the frequency change occurring on a system with an inertia time constant of five seconds. Another set of curves based on ten or 15 seconds might be desirable.

J. L. Blackburn and **H. J. Carlin**: The authors appreciate and thank all the discussers for their comments. The patent reference given by Mr. McConnell may have historical interest for the indicating-instrument designer but appears to have no bearing on the relay art. The design principles for an indicating device are entirely different from those of a frequency relay. An underfrequency relay, for example, must close contact at a certain preset frequency or below and not operate above that frequency. The frequency meter is not set for any specific frequency but must indicate continuously over its entire range. It should be noted that putting contacts on a frequency meter does not make a satisfactory frequency relay. The line of reasoning developed by Mr. McConnell would preclude the development of any new electrical device since they all use resistors, reactors, and condensers, and these were discovered long before 1900.

Mr. Steeb raises several points of importance to the operating engineer. The frequency-relay contact closing and opening points are essentially the same. The relay will reset to the maximum time lever setting when the frequency changes 0.1 to 0.2 cycles from the setting. For example, suppose the 60-cycle underfrequency relay is set to close its contacts at 59 cycles from the number 10 lever setting. The contact will start to move at 59.1 to 59.2 cycles but will not close until the frequency drops to 59 cycles or below. As the frequency rises just above 59 cycles, the contacts open but will not completely reset to the number 10 lever setting until the frequency rises to 59.1 or 59.2 cycles. With the time lever set at less than its maximum position, the 0.1- to 0.2-cycle differential is correspondingly less.

The stationary contact can be set with no follow to provide three- to six-cycles opening time. Since the relay has spring bias which opens the contact when the relay is de-energized, loss of potential will not cause the relay to operate.

In order to make a complete check of the relays a variable-frequency source would be required. To our knowledge, the nearest to a portable variable-frequency source is a low-frequency electronic oscillator. This should have a good sine-wave output. Perhaps a more convenient way to test these relays is to obtain them in Flexitest cases. Then the relay elements can be removed easily from the switchboard and taken to a central laboratory where variable-frequency equipment is available.

The general practice in taking time curves on relays is to assume an instantaneous change in the quantity to which the relay is responsive. This has been the procedure followed for the frequency relay. Although it would be desirable to plot time curves for various rates of change other than instantaneous, this would require a large family of curves. When one considers that the change of frequency time curve, after a disturbance on a system, generally would be nonlinear and vary considerably for different systems and different operating conditions, it can be seen that it would be difficult conveniently to present information that could be useful for all cases which might arise. The curves shown are a reliable indication of the operating time for an instantaneous or very sudden change in frequency. If the system frequency during a disturbance changes very slowly, the relay, of course, will take longer to operate, but this should not be objectionable, since the disturbance would not be so severe as that which would cause the more rapid change.

The example given by Mr. Logan is one where the system was flexible enough to adjust itself and be adjusted to the loss of sizable generation without the continued loss of frequency and instability. For this case the frequency relays if applied to the system would have been set below 59 cycles and for a time delay such that the short drop to 58 cycles would not cause operation. Thus, if the frequency continued to drop or if the external generator were not available and the frequency dropped below the setting for a period of time, load would be dropped as indicated in Figure 2 of the paper. As a matter of fact the frequency relay described has a time characteristic dependent on a combination of rate of frequency change and definite frequency drop as stated by Mr. George, whereas we understand that Mr. Logan's inertia relay operates on the rate only, except at low voltages where it would trip regardless of the frequency.

The frequency relay described is not designed to initiate reclosing operations but to co-ordinate with reclosing schemes. In general, such schemes are intended to reconnect system capacity as quickly as possible after an outage, and the frequency relay should be capable of being set with long enough time delay to ride through the reclosing cycles. Should the breakers fail to hold in after the reclosure, it then would be the function of the frequency relay to disconnect load on the loss of frequency. There may be special cases where the above reclosing practice does not apply, and in such cases the relay described may not be applicable. However, it is far from our intention to claim that the relay will solve all problems of this type and no doubt, Mr. Logan's inertia relay might have applications that our relay would not fill. It is maintained, however, that the new relay for a majority of frequency-relay applications is inherently better than any previously used in the art.

Current-Transformer Output and Application Charts

Discussion and author's closure of paper 44-93 by R. Koller, presented at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944, and published in AIEE TRANSACTIONS, 1944, August section, pages 573–7.

C. A. Woods, Jr. (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): This paper is a companion to the paper presented in 1942[1] by A. C. Schwager. It, like the paper presented in 1943[2] by S. D. Moreton, is a further analysis and extension of the principle of using the open-circuit excitation characteristics of a current transformer with negligible secondary leakage reactance for determining its performance as covered by the paper presented in 1940[3] by Mr. Bottonari and me.

The subject paper shows in detail the methods to be followed for obtaining useful curves of current-transformer performances from the admittance-vector locus. The author should be commended on the clear presentation of the limitations in output and accuracy of current transformers having a single-turn primary winding of low current ratings. The paper with Figures 4 to 7 shows quite clearly the effect of the output limits dictated by core saturation. Since core saturation occurs between 0.2 and 0.3 volt per turn per square inch of gross core area, the maximum volt-ampere input is the product of this voltage for the total core area and the primary current. The maximum secondary volt-ampere output is then less than the input by the excitation volt-amperes required to obtain the foregoing value of voltage. Thus, maximum output for any specific transformer and value of primary current requires that the turn ratio and burden impedance be properly matched to obtain the maximum value.

The practical importance of this from a relay-application viewpoint is in the selection of the proper relay tap for ground relays. The transformer ratio and line relays are often fixed by other factors such as normal load current and indicating instruments. The burden of the line relays is usually small and does not often require the maximum output, but the burden (in ohms) of the ground relay is usually high and a higher-current relay tap with lower ohmic burden value may often be more sensitive than a lower tap due to a closer approach to the optimum value for maximum transformer output. These conditions are further emphasized when taking into account the effect of the exciting current drawn by the secondaries of the remaining connected transformers located in the unfaulted phases.

Hence, while the information given in this paper is of considerable interest to design engineers, its use as a practical method of designating current-transformer performance may be somewhat limited because of a reluctance to become familiar with admittance vectors.

In reviewing the large amount of published information on the subject of current-transformer overcurrent performance, it would appear that the following conclusions may be drawn:

1. Current transformers having low equivalent secondary leakage reactance, such as bushing types, readily lend themselves to the use of the open-circuit excitation characteristics for portraying performance characteristics.

2. For most relay applications the algebraic rather than the vector addition of currents utilizing the simple open-circuit excitation curve is satisfactory since the errors introduced are usually less than those resulting from the variation of individual units from an average curve.

3. Excitation characteristics may be readily shown by means of the following curves:

(*a*). Curves showing secondary volts versus secondary amperes are most desirable for standard transformers, requiring a family of curves for multiratio transformers. However, a single curve for any specific transformer on a volt-per-turn–ampere-turn basis is almost as simple to use.

(*b*). Where vector addition rather than algebraic addition is required, such as in making phase-angle calculations, the excitation curve along with necessary data to determine the magnetizing and loss components of the exciting current all on an ampere-turn–volt-per-turn basis may be easily applied.

Such curves are relatively simple to use and are readily accepted due to the wide familiarity with the equivalent circuit and related vector diagrams of current transformers.

4. It would appear desirable that further effort be directed toward obtaining a widely accepted method of determining the equivalent secondary leakage reactance[4] of those current transformers where it is of appreciable magnitude, thus permitting use of the same desirable methods for determining the overcurrent performance on all types of current transformers.

References

1. Current-Transformer Performance Based on Admittance-Vector Locus, A. C. Schwager. AIEE Transactions, volume 61, 1942, January section, pages 26–30.

2. Bushing-Type Current Transformer Characteristics, S. D. Moreton. AIEE Transactions, volume 62, 1943, September section, pages 581–5.

3. Overcurrent Performance of Bushing-Type Current Transformers, C. A. Woods, Jr., S. A. Bottonari. AIEE Transactions, volume 59, 1940, September section, pages 554–60.

4. A Simple Method for Determination of Ratio Error and Phase Angle in Current Transformers, E. C. Wentz. AIEE Transactions, volume 60, 1941, October section, pages 949–54.

Otto A. Knopp (Pacific Gas and Electric Company, San Francisco, Calif.): Using A. C. Schwager's excellent paper[1] as a basis Mr. Koller has developed very ingenious charts which will be of great service to utility operating engineers in determining the performance of bushing-type multiratio current transformers for various primary currents and secondary burdens. Schwager's method thus becomes of increased practical value.

With this method errors are still apt to be quite large, however, unless, for one thing, the manufacturer's test data are taken on the individual transformer for which performance data are required by the utility engineer in order to determine relay settings. Also, even if the data are taken on the individual transformer, the aging of the iron or the history of the iron core in the transformer after installation and use may change the constants of the transformer an appreciable amount. Furthermore, the service load conditions of the transformers are in most cases not exactly known, principally due to the fact that the burdens of these transformers consist mostly of relays with iron cores having characteristics which vary more or less between individual relays of the same type. To determine these secondary burdens in the field involves considerable work and instrumentation.

Consequently, in view of the difficulties in obtaining accurate results by the use of the manufacturer's test data, it appears highly desirable to determine the characteristics of the bushing-type current transformers directly in the field, with its individual field burden. Such tests do unfortunately require considerable equipment, more than the burden test, some of which cannot be classed as portable. However, some years ago the writer developed semi-portable multirange current transformers and loading transformers for large current ratings combined in one unit[2] and a number of power companies are using such equipment for determining the characteristics of bushing-type current transformers in the field.

Therefore, the writer makes a suggestion which amounts to using the Schwager-Koller method in reverse. Instead of determining in the manufacturer's laboratory typical admittance-vector-locus charts and having the operating engineer use these charts to calculate for the particular field burdens the characteristics of the transformer in service, it is suggested that *some* of the characteristics be determined by test directly in the field, and that from these the admittance-vector-locus charts be determined. This would give the utility engineer the assurance that the charts apply to the individual transformer in question under its service and field-loading conditions. Each of these charts would constitute a permanent record for each individual transformer. It would then be possible at any time after the test to obtain from the charts additional data which were not taken at the time of the field tests. The need for frequent tests would thus be obviated. Only at such times as it appears advisable because of possible aging of the iron and because of severe overloads and short circuits impressed upon the transformers need the tests be done over to verify the original test results or to obtain the new somewhat altered characteristics of these transformers.

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1. Current-Transformer Performance Based on Admittance-Vector Locus, A. C. Schwager. AIEE Transactions, volume 61, 1942, January section, pages 26–30.

2. Some Applications of Instrument Transformers, O. A. Knopp. AIEE Transactions, volume 55, 1936, May section, pages 480–9.

R. Koller: The chief purpose of my paper was to develop simple but still generally applicable charts for the representation of bushing-type current-transformer performances. This could best be done by means of the admittance-vector method. Figure 2 of my paper completely solves the ratio-error data problem in a simple and general form. Figure 7 gives the essential information about secondary output. It should be emphasized that the use of these charts does not require any understanding of the admittance vector. These charts can even be obtained directly by tests without necessitating the use of the admittance-vector locus. On the other hand, if the admittance-vector locus is available the ratio-error and the output charts can be constructed from it in a few hours.

Mr. Woods' interesting analysis of the limitations in secondary output by core saturation gives a good explanation for the nonlinear relation between maximum secondary output and primary current.

Mr. Knopp's excellent suggestion for determining the performance charts for individual transformers in the field would greatly widen the applicability of the developed charts. It is to be hoped that a portable test set can be developed by means of which direct performance charts and the admittance-vector locus can be obtained at once.

A New Carrier Relaying System

Discussion and authors' closure of paper 44-135 by T. R. Halman, S. L. Goldsborough, H. W. Lensner, and A. F. Drompp, presented at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944, and published in AIEE TRANSACTIONS, 1944, August section, pages 568–72.

E. E. George (Southwest Power Pool, Little Rock, Ark.): Results with carrier-current protection of high-voltage transmission lines have been so successful that the only question today is what type of carrier protection to install, rather than whether to use carrier protection. The greatest possibility in reducing the cost of carrier protection is probably along the line of simplified relay equipment and simplified carrier equipment. This should provide a reduction in both investment and maintenance costs.

The scheme outlined by the authors seems to have considerable merit, but they should give credit for extensive development work on the same scheme to J. T. Logan and other engineers of the Georgia Power Company who have been working on improving carrier protection since 1937, and on this particular scheme since 1941.

Although this scheme follows in its fundamental principles the original carrier scheme developed by Fitzgerald, of the General

Electric Company, about 15 years ago, this fact is no reflection on the present development. Many of the most useful developments in the electrical field have resulted from further refinement of ideas which had already been tried, but discarded for the time being.

A lot of time and manpower would be saved if the AIEE committees responsible for technical programs could adopt the following objectives:

1. If one manufacturer offers a paper on a new development, all manufacturers and operating companies should have an opportunity to present papers on that development if they claim to have made important contributions to it. Such presentations should consist of *papers on the regular program* and not discussions.

2. Discussions should include something of more technical value than the mere statement that somebody else tried the same idea years ago but discarded it. All major manufacturers have been using up valuable time at AIEE meetings trying to attach priority and nonutility to their competitors' projects. Such time ought to be reserved for discussions on the material features of the project *in its present form*.

A. J. McConnell (General Electric Company, Schenectady, N. Y.): In describing the first carrier-current pilot-relaying system in 1927, A. S. Fitzgerald[1] introduced the fundamental principle of phase-angle comparison of the currents at two terminals of a transmission line, each terminal comparing and transmitting during alternate half cycles of the power-frequency current. Later, the operating record of equipments using this principle was published.[2,3] The equipment described in this paper uses the same fundamental principle.

The initial concept of the use of a phase-sequence network in conjunction with phase-comparison carrier relaying came in 1931. Figure 1 of this discussion is a wiring diagram of such equipment. As shown, the phase-sequence-network output was proportional to negative- and zero-phase-sequence currents, according to the expression, I_2+KI_0. Although not indicated in Figure 1, K was adjustable. With the interchange of two phase-current wires, the network output was proportional to I_1+KI_0. Equipments in accordance with Figure 1 were placed in service in 1933.

The operating record of these early installations demonstrated the soundness of the phase-comparison principle, but they were ultimately discontinued, principally because of excessive tube maintenance. However, present-day tube reliability should contribute to the success of phase-comparison carrier relaying, and the authors, together with J. T. Logan,[4] deserve much credit for its revival.

In all relaying systems there are certain limitations the neglect of which may result in improper application. The paper omits such information as minimum operating current on various types of faults, ability to operate sequentially, burden, and so forth. Consequently, limitations must be deduced, and perhaps incorrectly, from whatever information is available.

It would appear that under certain conditions the minimum operating current will be quite high. For example, if five amperes secondary current is the maximum full-load current, seven amperes would be a reasonable minimum pickup of the fault detector. The operating thyratron setting would then be 10.5 amperes. Bearing in mind that this means positive-phase-sequence current pickup, the positive-sequence current, for a fault at a given location, is approximately half as great for a phase-to-phase fault as for a three-phase fault. Thus, a three-phase fault current of approximately 21 amperes secondary current would provide just 10.5 amperes positive-phase-sequence current during a phase-to-phase fault at the same location.

In the application described in the paper, 21 amperes secondary current corresponds to 350,000 kva. For a fault at Trenton Channel on one of the four parallel lines, the minimum fault-current contribution from Warren station would have to be equivalent to 1,400,000 kva (that is, 350,000 kva per line) just to obtain relay pickup during a phase-to-phase fault.

If the three-phase short-circuit contribution from Warren station is less than 1,400,000 kva, sequential operation might reasonably be expected. However, there appears to be a blind spot with the system as described, because if the current from Warren station is between the operating values of the fault detector and the operating thyratron ($V2$), *both* terminals will be blocked, that is, locked in. This is because the carrier-current starting thyratron ($V1$), which blocks, can be shut off only by firing the other thryatron ($V2$). If $V2$ is not fired, $V1$ cannot shut off, and a continuous blocking signal is transmitted, thereby preventing tripping at Trenton Channel regardless of the current magnitude there.

This possibility of locking in on an internal fault would be especially serious on any system, such as a loop or network, fed from one point only. This is easiest seen by consideration of the simplest loop, a pair of parallel lines, fed from one end. During an internal fault on one line, the short-circuit current through the terminal away from the source can vary between zero and a maximum, depending on the fault location. Thus, the current through one terminal easily can be between the settings of the fault detector and the $V2$ thyratron.

It is stated that the paper describes a one- to three-cycle system. This statement appears to be extremely optimistic, and in fact, it can be stated that the equipment, as described, cannot operate in one cycle. With Figure 7 of the paper, relay-tube plate current does not appear until more than one cycle after the inception of the fault, even though operating voltage is present before that time. This might indicate that the fault-detector time is greater than the 0.75 cycle given. The fact that contact hash appears at the end of the fault-detector trace in Figure 8, rather than at the beginning, seems to indicate that circuit opening contacts were used to obtain this trace. The closing time of circuit closing contacts then would be greater than 0.75 cycle. The time of the trip relay adds to that of the fault detector, and it is doubted that the operating time of the trip relay can be reduced below about 1.5 cycles with reliability and lack of vibration. Diagrams G, H, and I of Figure 3 of the paper cannot be as shown, and the relay current, diagram I, must contain a large double-frequency component. The aforementioned is based upon the most favorable time of inception of the fault. Unfavorable timing of the fault results in the loss of up to one-half cycle. Further time will be lost if the currents entering the line terminals on an internal fault are out of phase, as indeed the positive-phase-sequence currents must be if the line carried load previous to the fault. It would appear that 2.5 to 3.5 cycles would be a more reasonable claim for the range of operating time.

It is admitted in the paper that the positive-plus-zero-sequence filter is subject to blind spots under certain conditions. Other statements, intended to minimize the importance of possible blind spots, seem at least questionable. For example, it is stated that the blind spot can occur only if *all* of the positive-phase sequence comes from one end of the line and *all* of zero-phase sequence comes from the other end. This is merely the worst or limiting case. The blind spot can occur if the zero-phase-sequence current at one end is *small* relative to the positive-phase-sequence current at that end. It may be claimed that, if there is any zero-phase-sequence current available at all, its effect can be magnified to overcome that of the positive-phase sequence. Theoretically, this may be so, but practically, it may be a dangerous procedure. If a true zero-phase-sequence current is magnified, so also will be false or fictitious zero-phase-sequence currents. Figure 7 of the paper shows a phase-to-phase fault during which there should be no ground current. The I_N current shown, therefore, is fictitious, and it is apparent that too great an amplification of this fictitious ground current could result in false operation. The paper further states that to have a blind spot, "magnitudes must be exactly right and the phase angle of the positive- and zero-sequence impedances must be equal." These statements are surprising in view of the fact that this system is a phase-angle system subtantially unaffected by magnitude above the $V2$ tube setting and in view of the 60-degree blocking zone shown in Figure 4 of the paper. Also, it is stated that the use of positive sequence alone is a solution. This is a solution only if there is no positive-phase-sequence current fed from one end of the line or if the positive-phase-sequence currents from both ends are above the positive-phase-sequence relay pickup. In either case there must be sufficient positive-phase-sequence current available during all types of faults, including single phase to ground.

In conclusion, I wish to make it perfectly clear that I believe that phase-comparison carrier relaying is fundamentally sound. However, in its present stage of development it cannot be considered as a cure-all, but designs which will extend its application undoubtedly will be available.

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3. DEVELOPMENT OF A RELAY PROTECTIVE SYSTEM. E. R. Stauffacher. AIEE TRANSACTIONS, volume 50, 1931, pages 80–8.

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T. A. Cramer (General Electric Company, Schenectady, N. Y.): In their description

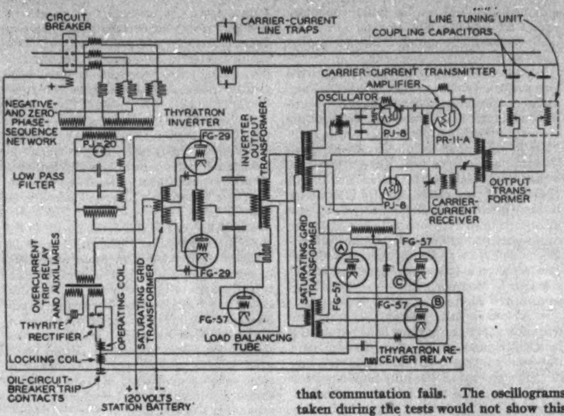
CIRCUIT BREAKER
CARRIER-CURRENT LINE TRAPS
LINE TUNING UNIT
COUPLING CAPACITORS
CARRIER-CURRENT TRANSMITTER
AMPLIFIER
OSCILLATOR
PJ-8
PR-11-A
NEGATIVE- AND ZERO-PHASE-SEQUENCE NETWORK
THYRATRON INVERTER
FG-29
INVERTER OUTPUT TRANSFORMER
PJ-20
LOW PASS FILTER
OUTPUT TRANS-FORMER
PJ-8
CARRIER-CURRENT RECEIVER
FG-57
A
C
B
FG-29
OVERCURRENT TRIP RELAY AND AUXILIARIES
OPERATING COIL
SATURATING GRID TRANSFORMER
SATURATING GRID TRANSFORMER
FG-57
FG-57
FG-57
THYRITE
RECTIFIER
LOAD BALANCING TUBE
THYRATRON RE-CEIVER RELAY
LOCKING COIL
OIL-CIRCUIT-BREAKER TRIP CONTACTS
+
-
120 VOLTS STATION BATTERY

Figure 1. Carrier-current pilot protective system

Vacuum-tube filaments are operated continuously from a storage battery. Thyratron filaments are operated continuously from the 115-volt a-c bus through a transformer

of two-frequency operation of the carrier channel, the authors state that the line trap at a given station is tuned to the frequency transmitted from that station. A carrier channel used for pilot relaying should be isolated at the receiving as well as the sending end. Although transmission takes place only when faults occur beyond the transmitter location, there are other causes which may reduce the received signal if the line is not trapped at the receiving end. For instance, if a circuit is opened at a location in such a way that an odd quarter wavelength stub is connected to the bus at the receiver location, the resulting open-end reflection may reduce the received signal below the required operating level. Difficulties of this type due to changes in system configuration can be avoided by tuning the line traps to both operating frequencies, thus completely isolating the channel.

The use of a thyratron position relay to control the transmitter and receiver provides the desirable characteristic of constant output regardless of current magnitude. When the 60-cycle grid voltage reaches the operating level of the tripping thyratron, the combination operates like a driven parallel inverter. With the simple excitation circuit shown on Figure 2 of the paper, the commutation time must be such that the relay will be able to follow the highest frequencies impressed on the control circuit. Failure of the thryatrons to commutate will result in both conducting current continuously, thus preventing tripping for internal faults. It would appear that the random closing of the plate circuit by the fault detector will result in cases where one tube is triggered just at the end of the cycle, and then the other tube is immediately triggered when the excitation reverses. With a high value of excitation this reversal may occur at such an extremely high rate that commutation fails. The oscillograms taken during the tests would not show this phenomenon, since the fault-detector contacts were in a different location from that shown in Figure 2. I wonder if it is proposed to use the circuit described for the tests rather than that shown in Figure 2? If so, the carrier-relay system would no longer be of the intermittent type.

J. T. Logan (Georgia Power Company, Atlanta, Ga.): It is gratifying to see the manufacturers again propose to employ the sequence method of fault-detection and line-terminal current polarity comparison by carrier as the medium for determining fault locations. Being unable to purchase a carrier relay system embodying these features, Georgia Power Company during the past three years has developed a system, one form of which is described in "Fault Current Polarities Compared by Carrier," reference 7 of the paper.

As for the Westinghouse Company's version of this system of carrier relaying, I regret that they have not been so free in passing along to me information concerning their system as I have in the past been in keeping their engineers informed regarding our experiences.

This is doubly regrettable inasmuch as we have had actual field experience during three lightning seasons. More than 200 tripping and blocking operations have been recorded by oscillographs, and, needless to say, these records have influenced us to make many changes in the electronic circuits. Just how the manufacturer expects to turn out an infallible scheme directly from the laboratory, without this valuable field experience to which they could have had access, would be of interest.

Apparently the system has been tested under conditions of solid faults only. Naturally those are ideal conditions. Arcing faults, those practically always encountered in the field, will afford the real test. I mention this because at least one feature in the electronic arrangement shown in Figure 2 of the paper we have eliminated from our scheme in the interest of reliability.

It is hoped that this carrier relay system will not have to undergo major circuit changes such as ours did based on field experience, but if a few failures to operate or incorrect operations occur, the basic scheme should not be condemned because it is definitely a step in the right direction.

T. R. Halman, S. L. Goldsborough, H. W. Lensner, and A. F. Drompp: With regard to Mr. Cramer's discussion, the use of single-frequency line traps for two-frequency operation is feasible in many installations where a carrier frequency can be selected which will give a received signal of adequate strength for all system conditions. In special cases, where the choice of frequency is limited and system switching results in too great a variation in received signal, double-frequency traps are justified.

The thyratron trigger circuit used in this relaying scheme has given reliable trouble-free operation throughout a very extensive series of laboratory and field tests. The time constant of the *RC* circuits is of the order of 0.0005 second, which allows dependable triggering and yet reduces the possibility of loss of control to a minimum.

As explained in the paper, the plate circuits of the two thyratrons were energized continuously for the field tests. This was done for the express purpose of obtaining a complete record of the trigger circuit operation from the very beginning of the fault. In a typical installation the fault detector would energize the trigger circuit only during a fault, and the carrier relaying system would be of the conventional intermittent type.

We are pleased to note the success Mr. Logan has had in applying our *HCB* relay to carrier. The article referred to by Mr. Logan did not appear until after the paper "A New Carrier Relaying System" had been submitted to the AIEE. Mr. Logan's article presents an interesting development on the same general subject.

With reference to Mr. McConnell's discussion, the sequence filter described in the paper has an output proportional to the positive-plus-zero-sequence components of the fault current. This is only one of several possible combinations of sequence components. It is desirable, as it allows operation on three-phase faults and provides increased sensitivity on ground faults. In applications where phase-to-phase fault-current magnitudes are below full load current, the use of positive- and negative-sequence components is a solution.

With regard to the possibility of blocking on an internal fault due to difference in current magnitude at the two ends of the line, recent developments have completely eliminated this condition. The margin of safety between fault-detector pickup and tripping current has still been maintained. This will allow correct tripping on an internal fault with any current distribution from the line terminals.

Under proper conditions this carrier relaying system can operate in one cycle. The fault detector and operating elements are high-speed polarized d-c devices which have an operating time of one-half cycle or slightly less when energized at two or three times minimum pickup. If the fault detector closes for an internal fault just as the sequence filter output voltage causes the operating thyratron *V*2 to fire, the total relay time will be approximately one cycle.

However, in the interest of reliability, the operating element has been slowed down intentionally, as mentioned in the paper.

The scheme shown in Figure 1 of McConnell's discussion uses a negative- and zero-sequence filter to control carrier and to operate the relay. A three-phase fault cannot be tripped when such an arrangement is used, except by the use of additional relay elements. This results in some of the simplicity of the scheme being lost and introduces a co-ordination problem.

The Evolution of Standard Lines of Current Transformers for High Overcurrent Capacity

Discussion and author's closure of paper 44-94 by E. C. Wentz, presented at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944, and published in AIEE TRANSACTIONS, 1944, September section, pages 658–62.

W. K. Dickinson (General Electric Company, West Lynn, Mass.): The subject of high overload-current capacity of current transformers has been one of much interest and has received considerable discussion in recent years—particularly the mechanical overcurrent capacity. In fact, there is still some question regarding the present American Standards Association definition of the term "mechanical overload limit." It is this angle of Mr. Wentz's paper that I would like to discuss briefly.

As the author points out, it is possible to calculate the forces (F) tending to disrupt the coils and also the strength (S) or resistance to the disrupting forces, but these calculations become rather complex. Furthermore, the actual values of F and S are subject to considerable variation, the former because of variations in the alignment of the coils and the latter resulting from variations in the strength of materials as well as manufacturing tolerances. Therefore, actual tests become the final criterion as to the proper mechanical overload rating.

The results of recent tests on 31 different transformers may be of interest. These tests were made on six types, covering a voltage range from 600 to 15,000 volts, as well as several different current ratings for each voltage class. For 29 of these transformers, a considerable number of tests were made on each, increasing the current in relatively small steps, in an endeavor to determine as closely as possible the exact point at which a distortion of the coils began. In every case, the first noticeable distortion was a bowing or radial stretching of the primary winding.

For the remaining two transformers, the initial current applied was much greater than calculations indicated they would stand without distortion. In both transformers axial distortion occurred similar to that described by Mr. Wentz. All of the transformers tested were standard stock units in which no special care was taken to align the coils. The shape of the primary coil was designed to be nearly circular, except for one flat side surrounded by the core, so that the radial distortion represents an actual stretching of the coil.

Based on the result of these tests, it is this writer's conclusion that in most cases a radial distortion constitutes the mechanical limit and that it occurs at a lower current than that which causes an axial distortion. This is somewhat in variance with Mr. Wentz's conclusions which are that the axial forces establish the mechanical limit. It is true that an axial distortion is likely to be more pronounced and destructive, whereas a slight radial distortion is not likely to cause immediate failure of the transformer. However, even a slight distortion should be considered as a failure since the insulation strength is decreased and the life of the transformer shortened.

Therefore, in establishing a mechanical overcurrent rating the value assigned should be a "withstand" value which is lower than the value causing radial distortion and can be demonstrated successfully in accordance with paragraph 4.046, American Standard *C*57-1943. The amount by which this assigned value is less than the value causing radial distortion may be considered a factor of safety which depends on variations inherent in the design, material, and method of manufacture.

Reference

1. American Standard *C*57, Transformers, Regulators, and Reactors, 1943.

F. J. Vogel (Illinois Institute of Technology, Chicago, Ill.): Many pieces of apparatus are built for special applications, even to the point where there are more special ones than standard. This takes engineering time and can be a source of field trouble as well. It is easy to go along in this way, but, if the reasons for the special equipment can be determined, it is possible to establish limits and a new line of standard designs to serve the purpose. This latter procedure requires initiative to find the problem and ability to analyze it before anything is done about it. This paper shows the value of attempting to determine the requirements for a new line of apparatus and then so designing the line that the major mechanical parts are standard for several different assemblies.

In the design, the subject of mechanical forces obviously has assumed great importance. Each of the problems described is met in the design of power transformers as well as of instrument transformers. In the case of core-type transformers, expanding force is present in the outer coils and results in tension in the copper. In the case of the inner coils, the force is contracting, and there is compression in the copper. This is exactly the same as if we had a cylindrical tank with external hydraulic pressure. Such a coil may collapse long before any great compressive stress is exerted on the copper. Formulas regarding vessels under external pressure involve usually comparatively simple conditions, a single material, and a knowledge of its modulus of elasticity. The transformer coil may be wound on an insulating tube and involve several layers of insulation and cooling ducts. Calculations using the copper alone lead to too conservative results, based on several tests. It has been found necessary to make tests and determine an empirical value of modulus of elasticity for such composite structures. Fortunately, except in a few special cases, the coils will be sufficiently strong, and failures from this cause are rare.

Fortunately, the unwinding force in most transformers is low, although there are some types of transformers where it must be considered and where it is not possible to obtain a rigorous solution for the mechanical structure. Recourse to tests again are required.

The solution for the axial forces is the most interesting one. The stress calculations made by designers often have been based on the position of the so-called electrical centers of both windings. The total expanding force has been calculated and has been considered to act at the electrical centers of both windings. If the electrical centers coincide axially, there should be no axial stress. Usually, a displacement of some arbitrary amount, say, one inch, is assumed, and the vertical component of the expanding force is used as the axial force. This method is incorrect, but tests to destruction of several models led to the conclusion that this method, although not rigorous or correct, gave a practical answer. Again, the trouble is that the actual centers are not fixed, the coil structure itself is not simple, and the bracing of the coils at the ends is not usually simple enough for exact strength calculations to be made. The author has determined the stresses more accurately, but still has to make tests to show the adequacy of the mechanical parts.

The three cases just described show that exact analysis of mechanical stresses in transformers is not possible, and the strengths of supporting structures to withstand them also cannot be estimated exactly. Hence tests are desirable to demonstrate the strength of typical structures. There is easily the possibility of single designs not of a standard line being weaker than intended.

Under short-circuit conditions in the field, the voltage at most power transformers is not maintained. Since the stress goes down very rapidly with decreased current, distribution and power transformers are not apt to be stressed up to their designed strength very often, and failures practically never occur. Nevertheless, it is desirable for manufacturers to make tests on sample or typical designs to be sure of their strength.

F. E. Davis (Commonwealth and Southern Corporation, Jackson, Mich.): Mr. Wentz in his paper brings out into the open two important factors relating to the manufacture of current transformers, and I will venture to say they also apply to many other lines of electric equipment:

1. The manufacturers make a considerable effort to supply their customers' needs economically as they are conceived to be, even when they seem quite special as compared to standard lines of equipment.

2. The demand for special current transformers is apparently so great that by applying exceptional ingenuity considerable cost saving is possible through a unification of the special requirements. Thus "standard" current transformers with "special" characteristics can be offered the industry at costs comparing favorably with standard lines although in some cases with impaired metering accuracy.

These factors also suggest the more obscure possibilities that more thorough engineering of the wiring situations in which the special transformers are required might eliminate or greatly reduce such requirements and the fact that manufacturers' standard lines of current transformers possibly might be incrementally broadened in application to eliminate some of the "standard specials."

For instance, it is somewhat surprising that transformers of such low current ratings as from 20 to 50 amperes in the insulation classes of from 5 to 15 kv are required on circuits of such high short-circuit capacities as a more or less general proposition. It would be interesting to know the details of such applications where a 20-ampere circuit would be used from a circuit or bus having a short-circuit duty of the order of 1,000,000 kva. It would seem that the necessity for high-voltage circuits of such low current ratings could be economically avoided at least in a good many cases through a judicious use of power transformers to supply utilization equipment of such comparatively small rating at lower voltages.

It will be noted that the maximum rating of the "special" transformers discussed by Mr. Wentz is 300 amperes. The standard line is suitable under the imposed conditions above this rating. This would seem to suggest that it may be practicable to increase the thermal and mechanical limits of transformers in the standard line of, say, 100 amperes and greater rating to meet the specified special conditions and thus substitute them for a good many of the special ratings. Then, if requirements for the special ratings of less than 100 amperes could be minimized by a more thorough engineering of the wiring systems and utilization equipments, the special current transformers required by the industry might become truly special, and improved over-all economy result.

While it is presumptious for me to comment on design and manufacturing processes of current transformers, Mr. Wentz's discussion of axial forces of the primary with respect to the secondary coils as being most destructive does suggest the possibility of reducing such axial stresses by a more accurate positioning of the two coils so as to be electrically centered with respect to each other even to the extent of less than the 0.1 inch he assumes. It would seem possible to position the coils by electrical comparative means and hold them in place by the injection of a suitable thermoplastic or similar material instead of with cement.

In this way it may be practical to extend the mechanical limit of some of the lower-rated current transformers in the standard line as previously suggested in order to substitute for some of the "specials." A closer co-operation of the users with the manufacturers will go a long way toward accomplishing the desirable result of the elimination of special requirements on the part of users for equipment used in appreciable quantities.

E. C. Wentz: Mr. Dickinson has found that nearly all the transformers he has tested fail by expansion of the primary coils. This shows that the transformers in question were sufficiently symmetrical that the axial force was not large enough to cause failure. However, it is not conclusive proof that other units would not be unsymmetrical and that they would not fail.

The point which neither of us can be sure of is the point about which we differ; that is, what degree of unsymmetry should be allowed for in giving the transformer a mechanical rating? Our experience has indicated that it cannot be assumed safely that the electrical center of the secondary coil is never displaced by less than 0.1 inch from the electrical center of the primary coil. This of course is a somewhat arbitrary value, but that it is hardly too large a value for safety can be estimated from a consideration of the following sources of dissymmetry:

1. Dissymmetry in the coils caused by misplacing the last (partial) layer.
2. Dissymmetry in assembly of the coils.

The last layer in the secondary coil is rarely full, especially in these designs where the secondary coils have to be all specially designed to go with a certain primary coil. Theoretically, it is possible to put this last partial layer in the center of the winding, but it is actually not practical for the winder to calculate exactly where to start the last partial layer for each coil. Each coil is slightly different. Our experience is that we can count on the winder placing the last partial layer within three-eighths inch of exact center. If the winding has only $2^1/_2$ layers (and some of the heavy current windings do), the average center may be displaced at least one-fifth of three-eighths, or 0.075 inch.

The error in centering the coils in their assembly can certainly be $^1/_{32}$ inch.

The results of Mr. Dickinson's tests can then be accounted for by supposing that:

1. He tested only standard designs, with perhaps five or six layer coils, with the last layer nearly full.
2. His method of holding the coils together has greater strength. This will be true if the design is made with one coil wound directly over the other, without separately insulated primary coils.
3. His coils are relatively less resistant to radial distortion.

This discussion has disclosed that the author should not have said "never" in relegating the expanding forces to a secondary position, but should have qualified it specifically.

There can be no argument as to the absolute magnitude of the forces; the only points to be cleared are:

1. Whether it is safe to assume a smaller initial displacement. I believe it is agreed that our assumption of 0.1 inch is safe, and that the exact "safe" value will always be a matter of opinion.
2. Whether it is safe to assume that the dynamic strength or resistance to very sudden motion can be assumed to be appreciably greater than the static strength. Our experience indicates not.

Protection of Large D-C Machines by Means of High-Speed Circuit Breakers

Discussion and authors' closure of paper 44-157 by J. Elmer Housley and Otto Jensen, presented at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944, and published in AIEE TRANSACTIONS, 1944, September section, pages 637–40.

R. C. Dickinson (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): It appears that the authors have done a good job in remedying an unhappy situation by means of high-speed breakers. However, the general value of this development to the industry is somewhat doubtful, since the generation of large amounts of d-c power is now quite generally accomplished with rectifiers instead of rotating machines. The performance of this breaker appears to compare favorably with other devices, which have been in production for more than 12 years. As shown by the authors' bibliography the value of high-speed breakers in limiting high d-c short circuits to less than the ultimate current has been known for a good many years, and devices of this type have been applied in continuous ratings up to 3,000 amperes for more than 20 years. It thus would seem unnecessary to use such high figures as 450,000 amperes from "a 300-volt 3,000-kw generator" and 700,000 amperes on an unstated number of machines presumably at 650 volts. There is no doubt that, although ultimate short-circuit currents in heavy d-c installations can be quite high, actual circuit and short-circuit resistances do not permit the extreme values given by the authors.

Tests on a 10,000-ampere 750-volt high-speed breaker in d-c high-power laboratory show a limitation time of 0.405 cycle (60-cycle basis) and complete interruption in 0.81 cycle on a short circuit on four 1,500 kw generators in parallel at 750 volts direct current. The ultimate short-circuit current is 80,000 amperes at 750 volts, and this high-speed breaker limits it to 32,000 amperes. The rate of rise is 6,000,000 to 7,000,000 amperes per second. This breaker was applied commercially in 1932, and a large number have been applied since then.

A 3,000-ampere high-speed breaker was tested in the field in 1925 on four 4,000-kw 620-volt rotary converters in parallel. The maximum current obtained with this breaker was 67,400 amperes on a carbon monoxide test. Limitation time was 0.46 cycle, and the complete interruption time was 0.8 cycle. The rate of rise was approximately 13,000,000 amperes per second. Although current limiting resistance is not used in the design of these breakers, the maximum bus voltage recorded in the above tests was only 1,200 volts with 620 volts normal.

J. Elmer Housley and **Otto Jensen:** We agree with Mr. Dickinson that we have found a happy solution to an unhappy situation. The trouble is there are so many other unhappy situations which require similar solutions. Mr. Dickinson does not agree with our figures of the ultimate short-circuit-current values. We are not too sure either, but the possibility of the current reaching the values we have mentioned seems to be there, and we should welcome the opportunity to stage a test in a modern installation to see whether or not high-speed circuit breakers are warranted.

We are happy to hear about the good performance of Mr. Dickinson's breakers but believe that we definitely have contributed to the art, partly by the simplicity of the design, partly by the numerous protective devices which can be incorporated in the breaker, and partly because it is a self-sufficient unit which does not require any external-cooling medium.

We are perfectly well aware of the trend toward rectifiers as the source of d-c power and have for several years manufactured the necessary equipment for their protection. However, it appears that for the time being rotary machinery must be used for driving the rolls in a modern mill, and the breaker was developed for this type of service.

A Pneumatic Mechanism for Outdoor Oil Circuit Breakers

Discussion and authors' closure of paper 44-102 by L. J. Linde and E. B. Rietz, presented at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944, and published in AIEE TRANSACTIONS, 1944, July section, pages 543–6.

A. W. Hill (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The authors have presented a very interesting discussion of the various methods of closing circuit breakers and have brought out particularly well the advantages obtained with the use of compressed air where modern high-speed breaker performance is required. It has been found that while pneumatic mechanisms represent somewhat of a change in maintenance problems, the trade in general has accepted this type of apparatus, and in fact showed a preference for it, especially for high-voltage outdoor breakers where improved system performance can easily be secured from the faster breaker reclosure. It has been found that a pneumatically trip-free mechanism built along the same lines has found so much favor with purchasers of this equipment, that during the past three years more than half of the oil breakers rated 115 kv and above built by the Westinghouse Company have been equipped for pneumatic operation.

The linkage shown in Figure 3 appears to be quite simple and easy to inspect and maintain. However, we might inquire whether on fast reclosing from a part-open position of the breaker would there not be a considerable impact at the moment the piston rod meets the roller on the toggle crank, inasmuch as the piston is rising freely with no load until it meets the toggle crank coming down as the breaker is being opened. It is recognized, of course, that air below the piston has considerable cushioning effect and it may be that the result is to give satisfactory performance in spite of the apparent tendency to strike hard.

Figure 5 shows the temperature rise at various points inside the mechanism housing when it is operating in an ambient temperature of −40 degrees Fahrenheit. Since this indicates a rise of approximately 80 degrees for measurements made at the check valve and control valve, we would expect that the temperature might become excessive in case the ambient rises to +50 degrees Fahrenheit at which point the thermostat operates. In other words, the temperature would then be 130 degrees, which over a long period of time would seem to be far more than would be desirable. We would ask whether such a high temperature rise is found necessary inasmuch as the air being used by the pneumatic mechanism naturally becomes comparatively dry as temperatures drop toward the freezing point, so that it would seem that such a temperature rise would not be needed after the air has dropped to a point well below freezing. It is also probable that leakage around the doors of the housing, when exposed to high winds, would have some effect on the temperatures inside the housing and we presume that the test reported in this paper covers observations made in a still atmosphere.

It is a little difficult to see from the illustrations, Figure 3, just how the dump-valve piston is held in the closed position when the pressure records, Figures 2A, 2B, and 2C, indicate full normal pressure in the cylinder. Apparently there is no pressure drop across the control or dump valves and without such a drop in pressure the illustrations do not explain why the dump valve will stay in the closed position.

While the reclosing time shown in Figure 2C is approximately 26 cycles, we presume that this same mechanism can also be used where faster reclosing is required. As pointed out earlier in this discussion, we feel that the pneumatically trip-free mechanism has a definite appeal to many operators as it provides the type of operation needed for fast opening on either straight opening operations or close–open operations as well as fast reclosing time.

L. J. Linde and **E. B. Rietz:** The mechanism linkage where the power unit is not connected to the closing linkage is typical of mechanism designs used very successfully by the General Electric Company for the past 20 years. It is quite true that the operating conditions with pneumatic mechanisms for high-speed reclosing are different than on previous solenoid and motor mechanisms. But, the pressure required to move a free piston is very low and, therefore, there is little energy stored up in this moving mass when it strikes the closing-linkage roller at part stroke. There is also the cushioning effect of the air under the piston, as Mr. Hill suggests, that helps to reduce the impact. The best proof of this design feature are the 200 mechanisms which have been in service for periods of up to four years. The majority of these are for 20-cycle-reclosing high-voltage breakers where the breaker completes only approximately half of the opening stroke on a reclosing operation.

The cold-temperature tests described in the paper show the most adverse temperature conditions. With a −40-degree-Fahrenheit ambient temperature the heaters are energized continuously. For higher ambient temperatures the heaters are energized only intermittently. This results from the use of housing heaters which control housing ambient temperatures and therefore the operation of the thermostat. With high ambient temperatures of 40 to 50 degrees Fahrenheit the heaters would be energized for only short periods of time and the temperatures of the compressor accessories would never become excessively high.

The cold-room test results that are recorded in the paper were made in a refrigerated room where the cooling was accomplished by circulating air with large fans. Therefore, these tests simulate outdoor conditions with moderate winds.

A comparison of the pressure records of Figures 2A, 2B, and 2C indicate cylinder pressures of 108 pounds with 120 pounds applied to the control valve. Although there is some leakage to the control valve there is a five- to eight-pound differential between the two sides of the dump valve so that it is positively biased to the closed position during a closing operation.

The time–travel curves show the mechanism adjusted for operation on a rated 30-cycle reclosing breaker. The same mechanism with different relaying and a slightly higher operating pressure will operate in less than 20 cycles reclosing time.

Fusing Practices on Distribution Systems—II

Discussion of paper 44-97 by John S. Parsons and J. M. Wallace, presented at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944, and published in AIEE TRANSACTIONS, 1944, August section, pages 598–600.

G. Fred Lincks (General Electric Company, Pittsfield, Mass.): The authors have presented some interesting data on the fusing of distribution circuits, above 5 kv in their paper. However, it appears to be rather a long jump from these data to the conclusions they have drawn.

Figure 1 of the paper shows that about 65 per cent of the 104 companies reporting are fusing with a minimum of one-, two-, and three-ampere fuse ratings as against about 35 per cent of the companies fusing with a minimum of five-ampere and higher ratings. Without a background of past practices this hardly could be interpreted as a present trend to higher fusing. Five-ampere minimum fusing has been practiced by some companies for many years. It is not intended that this discussion advocate either high or low fusing but rather that it enter again a word of caution to the authors' evident desire to further higher fusing practices. Judicious fusing of the distribution transformer involves numerous considerations pertinent to each particular system. These considerations are discussed in the article "Fusing Distribution Transformers."[3] A chart accompanying this article and Figure 1 of my discussion of the authors' previous paper[4] show definitely that a shift in fuse rating does affect the total clearing time sufficiently to cause an appreciable difference in the length of secondary line on which faults will be cleared quickly enough to protect the transformer, using the American Standard Association time–load curve as a guide.

In the last sentence of the paragraph under the heading, "Line Sectionalizing," they state, "This figure (Figure 4) shows that 63 per cent of the companies consider the use of more than three sectionalizing fuses in series to be impractical". Figure 4 does show that 25 per cent of the companies are connecting one or two fuses in series and probably, if asked, would report that this practice dates back 15 to 25 years. Line sectionalizing with more than one and two fuses in series is relatively recent in origin dating back about 12 years to the time when the more modern fuse links were made available.[1] Sectionalizing with three or more fuses in series requires a slightly more detailed study in choosing the correct fuse ratings.[2] Many operators who have made such installations with three to six fuses in series report that they have yet to have one false operation. With this in mind, Figure 4 might be interpreted to indicate the practicability of connecting three or more fuses in series as a means for economically improving service continuity, increasing revenue, and fostering consumer good will.

The authors' conclusion that small reclosing breakers or oil circuit reclosers will supplant repeating fuses, encourages a further prophesy that eventually all distribution circuits will be provided with the automatic reclosing and resetting protection against nonpersistent faults right out to the end of the line and to the ends of all of the branches. Single element fuse cutouts in the main feeder and the branch lines will isolate only persistent faults. If the fuse links in these cutouts are co-ordinated properly with the reclosers or the reclosing breaker at the substation so that each recloser or breaker performs its function first, the recloser will clear all nonpersistent faults and will reset automatically. Such a co-ordinated setup will require judicious fusing on the primary of the distribution transformers in order not to handicap the line sectionalizing. Something must and will be done to avoid the need for choosing between providing proper overcurrent protection to the transformer and minimizing fuse blowing by lightning.

Recently a co-ordination study was made on an actual 7,200/12,500 volt grounded-wye rural circuit. The available co-ordination data prepared by the manufacturer were used to compare oil circuit reclosers having instantaneous, time delay, and combined instantaneous and time-delay time–current characteristics in opening the circuit. Some favor has been directed toward the provision of time delay in at least some of the openings, while recognizing the advantages of the instantaneous opening in preventing the burning down of lines. However, the comparison just completed indicates that the disadvantages of time-delayed openings or combination instantaneous and time-delayed openings at least offset the advantages in the more modern concept of recloser-fuse co-ordination. Therefore they are not the final answer to the achievement of reducing the consumer minutes outage and the restoration expense to the minimum attainable, as is visioned in this prophesy.

References

1. Fuse-Link Improvement Gives Co-ordinated Protection. *Electrical World*. November 4, 1933.

2. Co-ordinating Fuses and Relays for Distribution Reliability. *Electrical World*, July 29, 1933.

3. Fusing Distribution Transformers, G. Fred Lincks. *Distribution*, April 1944, page 6.

4. Discussion by G. Fred Lincks of Fusing Practices on Distribution Systems—I, J. S. Parsons, J. M. Wallace. AIEE Transactions, volume 63, 1944, pages 467–9.

Fault Protection on Shipboard A-C Power-Distribution Systems

Discussion and authors' closure of paper 44-159 by H. G. Rickover and P. N. Ross, presented at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944, and published in AIEE TRANSACTIONS, 1944, pages 1109–20.

G. H. Welch (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): This paper covers a very interesting discussion of the shipboard power-distribution system requirements and the problems involved. During the last several years, considerable development work has been done to improve the reliability of these floating power plants, particularly those on naval vessels. On one class of vessels new circuit-breaker developments were incorporated which provided a complete selectivity system. Two main switchboards were involved, each fed by a single a-c generator, and the boards were interconnected by a bus-tie cable having a circuit breaker at each end. Feeder breakers were connected to each switchboard bus. Tests demonstrated that a fault on a feeder circuit would trip only the feeder breaker and not trip the local generator, tie, or remote-generator breakers. Faults on the tie circuit tripped only the breaker at each end of the tie cable. Bus faults tripped only the local generator breaker and tie circuit. This tripping selectivity was accomplished by mechanical time-delay mechanisms integral to the breakers. Other developments are now in progress which will extend these improvements in the future.

The following comments are offered in connection with the subject discussion.

Selective Tripping Operation

Selective operation of circuit breakers is usually more difficult of accomplishment on small vessels having a small number of generators of low capacity (for example, two 100-kw units) than on larger vessels involving a number of much higher-rated machines. This is due to the fact that on the smaller generator systems, the ratio of load inrush currents of the larger motors to the generator fault current is low as compared with that of the higher-capacity generator and distribution systems. This makes it difficult to arrive at tripping values for generator breakers which will provide discrimination between load transient currents and fault currents, with sufficient margin for reliable selectivity.

Differential Protection

Shipboard distribution systems involve numerous feeder circuits which are controlled by circuit breakers compactly arranged on switchboard panels. As no metering is required on these circuits, no current transformers are provided. Therefore, the installation of instrument transformers for use with a differential protection scheme would involve a major addition to the switchboard, requiring additional space, weight, and wiring complexity.

Reverse Power Relays

The use of reverse power relays for generators and generator cable fault protection would not protect the generator circuit to any great extent, but merely isolate it from the bus and its associated generators. In order to protect the generator completely, it would require automatic opening of the field circuit. Shipboard systems normally use a simple manually operated field switch to disconnect the alternator field from the exciter. To obtain the full benefits from the reverse power protective relays, it would be necessary to provide a circuit breaker or contactor having a trip circuit operated by the reverse power relay, in addition to the generator breaker tripping feature.

Operating Requirements

The operating conditions for shipboard equipment are much more severe than for corresponding shore installations. A wide range of ambient temperatures and high humidity are involved. The equipment must be compact and is usually mounted in restricted space. Satisfactory operation is required under conditions of shock and vibration and the pitching and rolling of the vessel. These factors have a considerable bearing upon the design of the equipment.

Herbert C. Graves, Jr. (I-T-E Circuit Breaker Company, Philadelphia, Pa.): It has been my privilege and pleasure to be in a position to watch the rapid improvement in Navy shipboard switchgear. No small item in this improvement has been the introduction of radical means for isolation of faults. It is the purpose of this discussion to outline briefly the background of these developments and their application to industry.

Before the United States entered the war, battle experience of other navies proved the weakness of the electric protective systems. Failure of power under such conditions may cause a serious loss of life and equipment.

The electrical section of the Bureau of Ships, under Captain Rickover, began intensive work on the subject. He requisitioned the co-operation of engineers of the three major circuit-breaker companies as an advisory committee to co-operate in finding a solution. This paper is the result of years of sustained effort by this group under the leadership of Bureau of Ships' personnel.

An actual system of this general type has been proved by the I-T-E Company by test. The results were highly gratifying. With the system as shown in Figure 1, of the paper four breakers operate selectively in series. The clearing time of any fault was less than four cycles even on the breakers with the longest time setting. All fault conditions with one to four generators cleared with no unnecessary loss of load.

This method of obtaining rapid and selective clearing of faults offers obvious advantages. Outstanding advantages may be briefly enumerated as follows:

1. High speed of clearing fault reduces fire hazard at point of fault.
2. High speed of clearing fault decreases duty on the circuit interrupter so that the rupturing capacity is less affected by the time interval of the protective short-circuit timer.
3. Cascading of breakers is obtained, conserving switchboard space but with a minimum loss of selectivity.
4. Backup protection is obtained with very short time intervals.
5. Overload protection, including that of motors started across the line, is independent of the timing of the short-circuit protector.
6. Correct functioning is obtained independent of the number or location of connected generators.

The paper presents a new system of protection. The devices for performing the functions have not yet been perfected. Their completion is expected shortly. In the meantime, I heartily recommend not only a perusal but a study of this paper by those of you who are responsible for application of protective devices for the isolation of faults.

R. W. Smith (General Electric Company, Philadelphia, Pa.): Captain Rickover and Mr. Ross are to be congratulated on the preparation and presentation of this excellent paper. They have given a clear and concise description of the basic rules and considerations in the design of ships service distribution systems. Also included are the optimum characteristics of switching and protective devices to make these distribution systems reliable in the performance of their function of supplying electric power to vital auxiliaries. Reliability of the power supply on a combatant vessel is immeasurably more important than for a comparable land installation, because failure of the electric system for even a few minutes during the stress of battle may mean the difference between victory and defeat. Through Captain Rickover's efforts the ideas and developments of the various switchgear manufacturers have been correlated closely into a joint endeavor to reach the objectives outlined in this paper in a minimum time. Since the beginning of the war many changes and improvements in the electric distribution systems have been accomplished, not only on new construction but also on the older ships. Other changes and improvements are now under way, and still others will follow as better devices and methods are developed and made available.

Most of the protective devices developed primarily for the Navy also are or can be made applicable for use in ordinary power generation and distribution systems, and after the war they will undoubtedly be so used. Although the details of specific devices cannot be given now, it is of interest to know that the switchgear manufacturers have developed and are supplying direct-acting overcurrent tripping means for air circuit breakers which accomplish many of the objectives outlined in the subject paper. Some of these devices give only time-delay and instantaneous short-circuit protection with a time–current characteristic such as shown by curve B of Figure 8 of the paper. Others give time-delay overload together with time-delay and instantaneous short-circuit protection, and these have a time–current characteristic very much like the ideal characteristic as shown by Figure 9 of this paper.

At the present time the switchgear manufacturers are working on improvements in these devices with the specific objective of simplification of design together with a reduction in the maintenance required.

It is hoped and expected that with the close co-operation of Captain Rickover and his associates in the Bureau of Ships, the manufacturers can complete present developments at an early date and undertake still other improvements not yet conceived.

E. L. Harder (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): This paper demonstrates the application to naval-ship systems of the latest and best techniques of fault-current calculations and co-ordination of tripping devices. It shows a recognition and understanding of the basic elements of the problem and a sound engineering approach to the solution.

Undoubtedly shore establishments also will benefit greatly from the developments described by the authors. For example, the need for selective tripping of low-voltage air circuit breakers is of long standing, and the solutions to this problem, borne of necessity for naval service, undoubtedly will carry over into the utility and industrial fields.

The authors indicate that differential protection is widely used for busses in shore service. This applies more particularly to higher-voltage systems. On systems of 600 volts and less, which are comparable to shipboard systems, the same urge will be found to exist for low-voltage selective air circuit breakers as on shipboard.

It is surprising to note that on shipboard, bus faults are among the most common types. In shore practice bus and apparatus faults are rare as compared with line and cable faults.

H. G. Rickover and **P. N. Ross:** The authors wish to express their appreciation for the interest shown in their paper and for the favorable comments and discussion which have been presented. To a very large degree the progress and developments made in the protection of low-voltage a-c power systems, which are reflected in this paper, have been the result of the combined efforts of the various switchgear manufacturers working in close co-operation with the Bureau of Ships. In this way it was possible:

1. To establish the limitations of protective devices previously in common use both in the Navy and elsewhere.

2. To establish the basic requirements for an adequate fault protective system.

3. To develop devices capable of meeting these requirements.

It is noteworthy that the principles and methods of fault protection presented in the paper are adaptable to, and will find application in, the utility and industrial fields. It is hoped that out of the experience thereby obtained further improvements will be developed which in turn can be applied to naval and marine service.

Improved Selective Tripping of Low-Voltage Air Circuit Breakers

Discussion and author's closure of paper 44-158 by Charles P. West, presented at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944, and published in AIEE TRANSACTIONS, 1944, August section, pages 608–10.

L. C. Peterman (nonmember; Ford, Bacon, and Davis, Inc., New York, N. Y.): The announcement made in this paper of the new selective tripping mechanism for air circuit breakers indicates that we now have low-voltage direct-tripping devices that will give a performance on overloads comparable to that secured with relays, shunt trip and storage battery, such as customarily are applied only to high-voltage circuits. Further, the inherent nature and simplicity of the direct-acting trip should give high-speed action where that is required, equal to the highest speed now available from the relay–shunt-trip combination, coupled with great reliability due to the smaller number of parts involved in the tripping action.

The author does not indicate whether the tripping point of the new breakers can be adjusted in the field, after leaving the factory, or whether each specific rating must be built into the tripping mechanism at the factory. Unless tripping points can be selected in the field, within a reasonable range, the subject improvement will have limited applications. The present inflexibility of application of air breakers to changing loads is a great handicap. If the improvement mentioned does not eliminate this handicap, such an objective should be, in the writer's opinion, next on the list of developments planned.

R. W. Smith (General Electric Company, Philadelphia, Pa.): Cascading of low-voltage air circuit breakers is a well-established practice and was quite completely discussed in AIEE technical paper 41-128 by A. E. Anderson and C. H. Black at the 1941 summer technical meeting in Toronto.[1] This practice is reviewed by the subject paper, and its advantages and disadvantages are clearly restated.

Not so clear, however, is the discussion relative to the application of cascading principles nor the discussion relative to the use of definite-time-tripping devices. It must be remembered that the use of definite-time-tripping devices does not make possible the attainment of complete selectivity in the tripping of breakers in cascade arrangement under all short-circuit conditions. Any arrangement of breakers cascaded for economic reasons basically has breakers with instantaneous trips operative at currents below the available short-circuit current.

Also, it must be remembered that as long as breakers contain the instantaneous-trip feature at 12 to 15 times their rating, complete selectivity is not possible at short-circuit currents beyond these values.

Definite-time-tripping devices properly designed to give more accurate settings than present time-delay devices will make possible better selectivity between breakers for currents below the instantaneous-tripping values. The use of more accurate timing devices would be particularly advantageous where fault protection requires pickup settings lower than the instantaneous setting. For example such time-delay tripping devices could be used on backup breakers where high-impedance bus faults could occur with a short-circuit current below the instantaneous setting of the backup breakers.

REFERENCE

1. PROTECTION OF LOW-VOLTAGE CIRCUITS BY AIR CIRCUIT BREAKERS IN CASCADE ARRANGEMENT, A. E. Anderson, C. H. Black. AIEE TRANSACTIONS, volume 60, 1941, pages 1151–60.

L. W. Clark (The Detroit Edison Company, Detroit, Mich.): The development of a combination instantaneous and definite-time-delay tripping device incorporated in the design of low-voltage air circuit breakers should broaden considerably their application in the industrial field. In present designs where oil dashpots or thermal devices are used for obtaining time delay, it is difficult, if not actually impossible, to adjust the settings with any degree of accuracy in the attempt to make one breaker selective with another. This new tripping device definitely should increase the usefulness of air circuit breakers, providing it proves to be rugged and of low cost, requires a minimum of maintenance in the field, and has accurate and constant timing characteristics. Perhaps Mr. West can describe the device, indicating how the time delay is obtained and give us some supporting evidence as to its accuracy and durability in the field.

During the past few years I believe there has been a definite trend toward the use of unduly complicated industrial-plant distribution systems in the attempt to provide better continuity of service. There may be some few applications where such complication has been justified, because of the nature of the industrial process, but, by and large, the simple scheme with a minimum of equipment of rugged design is to be preferred. A simple radial distribution system incorporating selectivity in breaker settings, such as would be possible with this new device, should not only prove adequate for most installations, but also actually give better service through the years than some of the more complex network systems.

Troubles due to faulted circuits do not occur very often in industrial installations, and, when they do occur, service can be restored much more quickly with the simple system than it can with the more complicated system. One case recently studied involved a complete industrial network system, whereas, a simple radial system would have been cheaper and much easier to operate and maintain. A short-circuit study indicated that a fault in any one of several locations might open as many as four or five fuses and circuit breakers. In these circumstances it would be extremely difficult for the plant electrician to locate the trouble and restore service. It would seem much more desirable to use a simple radial system of plant distribution wherever possible, and then, when a fault occurs, there is no question about its location, and the trouble can be quickly located and repaired. The development of this new tripping attachment should assist greatly in the laying out of such a simple radial system, using either the cascade arrangement of breakers where it is desirable to keep breaker costs at a minimum, or adequate rupturing capacity breakers throughout, and providing definite selectivity between different breaker locations.

In this connection is it possible to block out the instantaneous feature of the trip if it is not needed? Also, is it possible to adjust the time delay to either the 0.2-second value or the 0.4-second value, or must a definite value be specified at the time of purchase?

Charles P. West: Clark and Peterman raise the question as to the type of the time-delay device which was described for low-voltage air circuit breakers and the means for adjustment which are available. The time delay is attained by means of a mechanical escapement, the time interval being determined by the number of oscillations of the moving element. It is sufficiently accurate to obtain time discrimination between breakers in a cascaded arrangement where the time intervals would differ by less than one tenth of a second. Normal applications would be expected to have time differences slightly greater than this. Its mechanical durability would be co-ordinated with the breaker to which it is applied.

The time-delay feature can be adjusted. It is also possible to block out the instantaneous trip. However, this change is not recommended, as breakers must be allowed to trip instantaneously if their short-time or interrupting ratings are exceeded. If the currents are below these values, the instantaneous trip does no harm, and, if they are higher, it is a necessity. It therefore would appear desirable to retain it.

The range of trip-current adjustment of the breakers to which these time-delay tripping devices are applied is the same as now commercially available.

Load-Calculation Procedure for Electric-Panel Space Heating

Discussion of paper 44-154 by B. F. Raber and F. W. Hutchinson, presented at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944, and published in AIEE TRANSACTIONS, 1944, pages 1085–91.

G. W. Penney (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): I believe the authors are to be complimented for making such extensive calculations on the radiant transfer of heat between the various surfaces of a room. It is instructive to consider a basic equation for interchange of heat between surfaces as given in equation 1, even if its actual solution is so difficult that in the numerical calculations tabulated values obtained by graphical means must be resorted to. However, I question if these elaborate calculations for interchange by radiation are justified, unless the assumptions used in calculating the interchange by convection can be improved.

Heat loss by convection usually is represented by a constant multiplied by the temperature difference to approximately the 1.25 power, or, if equation 8 is to be used, the constant h_c should vary as the 0.25 power of the temperature difference.[1] Equation 8 as given in the paper assumes that the heat transfer is proportional to the first power of the temperature difference. I suppose this is done to simplify the solution of the simultaneous equations. However, in this case, I believe, the authors should suggest some means for selecting the proper value of h_c for the particular temperature difference obtained.

The assumption of a given temperature, that is, uniform temperature throughout the room, is another assumption which I believe is very far from the actual condition, particularly, in the example chosen where the heat is supplied by means of heated ceiling panels. Several experimenters have measured temperatures throughout rooms and found that the temperature gradient from floor to ceiling is almost never negligible and is particularly high in the case of rooms heated by ceiling panels. Randolph and Wallace[2] published tests on a house heated by ceiling panels and having walls insulated by two-inch-thick bats of balsa wood. Figure 9 of their paper gives a temperature difference of seven degrees between air three inches above the floor and three inches below the ceiling, when the difference between outdoor and indoor temperature is 60 degrees Fahrenheit. Under these conditions the difference between the air three inches below the ceiling and the temperature of the ceiling is less than five degrees. In other words, the difference in temperature within the room is greater than the difference in temperature between the ceiling and air three inches below the ceiling, and this in a building where the heat losses would be much less than the case picked by the authors of the present paper.

Houghten, Gutberlet, and Hach[3] have published extensive data on panel-heated rooms. Their test number 18 gives temperatures in a room heated by ceiling panels when the outside air temperature was eight degrees Fahrenheit. Under this condition the panel in the ceiling was 107 degrees Fahrenheit, while the air six inches below the ceiling was 84 degrees Fahrenheit, and six inches above the floor was 70 degrees Fahrenheit. In other words, there was a difference of 14 degrees Fahrenheit between air six inches above the floor and air six inches below the ceiling. When this variation in air temperature within the room is considered, it will be found that the wall temperature varies from floor to ceiling so that we can no longer consider a wall as having a single temperature.

Usual calculations made by heating and ventilating engineers are for the purpose of estimating the over-all heat loss, and these calculations can be quite approximate. Consequently, the assumptions are very approximate in the interest of simplicity. In these usual approximate calculations the variation in temperature from floor to ceiling is neglected. Likewise the expression for heat loss through walls is quite approximate. However, if we are to attempt to calculate the wall and ceiling temperatures within a room, our assumptions must be much more precise.

When the inaccuracies in the assumptions regarding convection are considered, I do not believe that the calculations should be relied upon to compare heat losses by convection and radiation to the degree of accuracy implied in the statement that "the saving due to use of radiant heating" is 1.6 per cent. I think it would be very interesting to apply the methods of calculation used in this paper to some condition for which detailed test results are available such as the test given in the paper by Houghten, Gutberlet, and Hach. Possibly the inaccuracies in the assumptions could be compensated for by a proper choice of empirical constants in the equations. The discussion of the possible means of adjusting these constants would be quite interesting.

In theory radiant heating offers many interesting possibilities. Over ten years ago L. W. Schad conducted tests in the Westinghouse research laboratories which demonstrated that comfort conditions could be obtained with an air temperature lower than 60 degrees Fahrenheit by using heated panels in both walls and ceiling. However, the large surface of heated panels warmed the air to such an extent that this temperature difference between walls and air could only be obtained by supplying approximately ten changes per hour, which is not practical or desirable in the home, although it may offer an interesting possibility in auditoriums and other places where much fresh air is required.

Doctor Mills at the University of Cincinnati has demonstrated the possibility of securing comfort conditions by the use of highly reflecting walls and a relatively small radiant source. If the walls could have perfect reflectivity, a small heated panel could give a high mean radiant temperature, even though the reflecting areas were not warmed. This would be a very interesting possibility, if practical colored surfaces could be obtained having a high reflectivity for the infrared radiation corresponding to a surface at about 100 degrees Fahrenheit. At present the only available surfaces having good reflectivity in this range are bright metallic surfaces, and most of us do not care to live in rooms with all walls, ceiling, floors, and furniture having bright metal surfaces.

A primary object of any heating system is to provide comfort conditions in all parts of a room. Large areas of single glass normally give a low effective temperature near the window. Heated panels mounted below or near the window can be used to compensate for the cold glass and aid in obtaining comfort in all parts of the room. However, modern building practice is tending rapidly toward well-insulated walls and double glass, and in this case, I believe, the difference between radiant heat and a good convection system is largely academic. Large heated panel surfaces automatically heat the air to nearly panel temperature. In the case of convection the loss of heat through walls is small so that the walls come nearly to air temperature.

In conclusion, I believe that further refinement in the means of calculating heat transfer by convection is needed before we are in a position to calculate accurately the various wall temperatures to be expected in a panel-heated room. With modern well-insulated houses, very satisfactory comfort conditions can be obtained by either a good radiation or a good convection heating system. In my opinion a choice between the two systems is more likely to depend on such factors as cost of installation, convenience of operation and control, and the particular desires of the individual user.

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3. Radiation as a Factor in the Feeling of Warmth in Convection, Radiator, and Panel Heated Rooms, F. C. Houghten, C. Gutberlet, E. C. Hach. American Society of Heating and Ventilating Engineers *Transactions*, 1942, page 55.

T. F. Peterson (American Steel and Wire Company, Cleveland, Ohio): For the past few years I have been conducting in my home recreation room some experiments designed to demonstrate the effectiveness of panel heating developed by current flow over surface resistors. The work stems from some 15 years of development and research on semiconducting films or layers used for static shielding of cable, elimination of static in general, and so forth. For the most part these films are made up of electrical insulating materials such as rubber, synthetic resins, varnishes, fabrics, and the like, which contain conducting particles (such as acetylene carbon black) in sufficient amount and properly dispersed to yield volume or surface resistivities of the order of so-called semiconductors. Fabrics coated with these materials may yield surface resistivities from several ohms to millions of ohms per square inch.

One form of heating panel which I have studied consists of a fabric (either cotton or glass) coated with semiconducting resins such as nylon, alkyd, or neoprene, fastened to a high-thermal-resistance wall board and having fine steel wires contacting the resistor coating at intervals of 20 inches to 36 inches. The spacing of these parallel wires is chosen so that the panel may be connected across 110 volts and develop a surface temperature between 85 degrees and 100 degrees Fahrenheit. The high thermal resistance of the backing material yields a structure with low effective thermal capacity. The semiconducting coating insures uniform distribution of heat generation. When this latter coating is covered with an electrical insulating varnish, paper, or the like, having low emissivity, the efficiency of the panel is improved. Even without such coating tests indicate that energy consumption as low as 0.15 to 0.20 watt per square inch results in the type of panel heating normally sought.

I recognize many of the weaknesses of the system such as shock hazards and fire possibilities due to poor contact by the wires and semiconducting layer, but none of these appears insurmountable. Materials are available which are not only flameproof, but very resistant to oxidation, warping, aging, and so forth, and surface coatings may be applied to these to provide not only electrical insulation but low emissivity (high reflectivity) toward the center of the room.

The authors have made a valuable contribution to the general theory of panel heating, and it is hoped that this practical design may add to the over-all worth of their work.

F. L. Lawton (Aluminum Company of Canada, Ltd., Montreal, Quebec, Canada): Having been closely associated with a large-scale electric house-heating experiment in northern Quebec during the 30's, in the course of which some 60 homes were heated by various schemes for the direct application of electric energy, I have been pleased to note the three papers just presented.

The authors are to be congratulated on the excellence of their presentation of this subject especially timely for the immediate postwar period. The Institute's committee on domestic and commercial applications has done a fine job in securing this group of papers which bring right to the fore the possibilities of electric house heating.

Raber and Hutchinson might have added to their analysis of the part panel heating will play in the field of electric heating for homes by developing the effect of improved insulation in walls, floors, and ceilings beyond present practices at very moderate cost, thereby decreasing appreciably the winter-heating and summer-cooling energy requirements.

Direct application of electricity to house heating is approaching a strictly competitive position with respect to coal, oil, or gas-fuel heating in areas of high fuel cost and low rates for domestic electric energy, where, for example, abundant hydroelectric power is available. For instance, based on average seasonal efficiencies of conversion, 13,500 Btu per pound coal at $15 per ton and 140,000 Btu per United States gallon of fuel oil at 10 cents per gallon compare with electric energy at 0.4 cent per kilowatt-hour. In some favorable areas public utilities today are finding it satisfactorily good business to sell domestic energy in blocks over 350 kilowatt-hours per month at 0.75 to 1.0 cent per kilowatt-hour.

It thus will be seen that the margin between prices for electric energy and ordinary heating fuels is relatively narrow in certain areas.

Panel heating, electrothermal space heating, and the heat pump offer definite indications that this gap may be eliminated rapidly in the postwar period.

Even with the coefficient of performance for a heat-pump system as low as 2.0, such as would apply in the colder areas of Canada, electric heating utilizing the heat pump can compete strictly on a price basis with coal or fuel oil where domestic energy is available at low rates. In addition, it has advantages of convenience, ease of control, and cleanliness which no other fuel can approach.

Those interested will find considerable useful information on electric house heating secured during 1928–38 from a number of installations in the Saguenay area of Quebec and reported in the advance reports and *Proceedings* of the Canadian Electrical Association.

The possibilities for improved comfort and cleanliness in the heating and cooling of postwar homes offer a real challenge to our profession, manufacturers, and utilities to provide the necessary low-cost equipment and service and thereby develop a business which well may play as large a part after this war as radio did following the first World War.

Possibilities of Heat Pumps for Heating Homes

Discussion of paper 44-156 by R. U. Berry, presented at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944, and published in AIEE TRANSACTIONS, 1944, September section, pages 619–22.

Victor Siegfried (Worcester Polytechnic Institute, Worcester, Mass.): This paper gives a very interesting account of the methods for obtaining more effective heating other than direct space heating by electricity. Of especial interest to me are the fuel-conservation implications which may be inferred from it.

With a favorable coefficient of performance as described, the use of electricity as an activator for a heating plant appears to suggest possible over-all economies and conservation of natural fuels where surplus electric power is available. It may further be found more economical to purchase even steam-produced electricity from coal rather than to consume high-grade fuels in domestic furnaces of relatively low efficiency. It has appeared to be little short of criminal, especially in the war period, to be consuming liquid fuels for domestic heating, and a long-range view of uses for oils and gasoline cannot but support an extension of that same alarm to later periods. Since the liquid fuels are predominantly adapted to mobile power plants, their use in fixed installations certainly should be discouraged.

The use of the heat pump would seem to permit a return to the convenience and muss-free advantages of the oil-fired domestic heating plant without jeopardizing fuel resources thereby. Other advantages outlined in the paper with regard to reversibility for cooling service give this equipment attractiveness for an engineer who would like to see the same breadth of interest applied to engineering of the mechanics of living that has been given industrial applications. Certainly the reputation of the profession would be greatly enhanced in the eyes of the nonengineering consumers by having fully engineered living facilites developed, which are more co-ordinated than are present-day devices.

In addition to the progress in the application of equipment to this problem of heating, it seems to me that the industry in general and the electrical-engineering profession in particular are challenged to find ways in which the equipment can be furnished and fed with power at prices and rates which are attractive. Somewhere in our development of these ideas we must have the courage to reduce prices and rates below their apparently proper levels in order to stimulate the very markets which later will justify these "introduction prices." It would seem to me that too often the progress in such advances is hindered by the price angle, even though the engineer can foresee a volume-cost value which he could have proved if given the chance. Who is to take the risk is an unanswerable question, but the opportunity is for the *engineer* to be more outspoken in support of the newer ideas that may not at the moment appear to be economical. The need is thus for *vision* and shrewd judgment on the part of the engineer to counteract natural conservatism on the part of business management.

C. I. Hall (General Electric Company, Schenectady, N. Y.): A review of this paper brings to mind a number of intriguing possibilities in connection with the complete engineering of the modern home. The development of household utilities so far has been along the line of the individual devices with practically no co-ordination of their various functions. If, however, we look at the modern home as a complete engineering problem, we are confronted with the various uses of energy in the applications listed below.

1. Heating.
2. Cooling.
3. Hot water.
4. Domestic refrigeration.
5. Quick-freeze cabinets for food storage.

All of these functions are closely interrelated so far as production of the effect is concerned, and in a completely engineered home there is a possibility that one or more of them may be derived as a by-product, with possibly a reduction in cost both in operation and required apparatus.

If the major heating of the home can be obtained from the central-station system as outlined in this paper, then all of the functions outlined may be performed either directly or indirectly from the electrical service.

One of the important considerations in applying such a co-ordination of activities is the matter of reliability. Then all of the eggs will be in one basket, and service and apparatus failure will be proportionately more bothersome. However, such advances have been made in the maintenance of service of central stations that outages of appreciable duration are now very rare. Similarly, improvements in modern design and manufacturing technique have improved greatly the reliability of the mechanisms to perform the various functions that have been outlined. Such improvements, of course, will continue.

Consideration of the source of heat for electric house heating must take into account the very serious drop in the water table in the Eastern States. The draft upon the water reserve has been so great that in a number of states it has been necessary to pass regulations requiring the return of process and cooling water to the ground. It would appear, therefore, that it would be desirable to utilize some other sources of heat for the refrigeration cycle, since in general the application of this system will be made in larger communities where water availability is at a minimum.

Electrothermal Space Heating and Air Conditioning

Discussion and author's closure of paper 44-155 by George H. Krueger, presented at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944, and published in AIEE TRANSACTIONS, 1944, October section, pages 718–22.

R. R. Herrmann (Northern States Power Company, Minneapolis, Minn.): I for one am particularly glad to see the investigation of house-heating equipment which is now going on. Some of the crudest of applications of engineering, if we can call it such, have in the past been made on house-heating furnaces. Engineers have improved industrial power plants very materially but little has been done in a similar manner with household furnaces.

When we speak of off-peak energy or off-peak rates, the problem is not as simple as it sounds. Care should be taken, as Mr. Leatham pointed out, that the off-peak load will not become the on-peak. If the off-peak business being secured comes on in large volumes and at a much more rapid pace than the regular business it will not take long before it overtakes the increase in the ordinary business.

Perhaps we should take a leaf from our fellow utilitarians in the natural-gas business who have what are known as interruptible gas rates for industries. When necessary, because of the heavy use of gas by house-heating customers, gas to these interruptible customers is discontinued. Perhaps the electrical industry can do something similar.

In the case of a water power which has a surplus amount of water which would be wasted if not utilized at the time, anything which the utility can secure from the sale of surplus water is of course just so much velvet. I understand that the Winnipeg hydro system is in this position and is able to sell a considerable amount of energy to the local steam-heating system, which energy is used to generate steam.

I notice that the house described by Mr. Krueger requires 35,000 Btu per hour. This is slightly over ten kilowatts. Inasmuch as this service is off-peak it cannot be on continuously and some greater amount of capacity than ten kilowatts will be required; yet 14 kw only is provided. It seems that this is not sufficient, or perhaps the 35,000 Btu per hour is more than necessary.

There are a few general factors which are not favorable toward the house-heating load. One of these is the poor annual load factor. Sufficient capacity must be provided for the coldest day, which, in a climate like Minnesota which has about 8,000 degree days per year, may be 30 degrees below zero, which means that slightly over one per cent of the annual amount of energy required will be used in the coldest day. Furthermore, there is little diversity in this business. When it gets cold at one spot in a city it gets cold all over so that this contributes again toward its undesirability.

It has always appeared to me that the development of a cooling system in conjunction with the heating system has the best possibilities for a utility. By this means the utility has assurance of a load in both cold and hot weather.

In considering heating of houses by means of electricity we must remember that this is not new. There are a considerable number of houses heated by this means and a considerable number of experiments have been made. By storing heat in some manner which can be released on extremely cold days, the annual load factor can be increased. In a climate such as Minnesota, however, where subzero weather is likely to exist for three or four days in succession, it appears that the amount of heat which must be stored to take care of such long cold weather would have to be extremely large.

S. J. Levine (General Electric Company, Bloomfield, N. J.): The system of electric-heating equipment proposed in this paper is well described and constitutes an ingenious multipurpose scheme. More extensive information on operating costs would be helpful, particularly as compared with other fuels, and on equipment costs as compared with other equipments. Another interesting comparison might be made against other forms of electric heating and cooling equipment as regards first cost and operating cost, particularly as regards the thermo compressor versus the conventional electric-driven vapor compression system. The latter has a higher coefficient of performance than the thermo compressor and would therefore be more economical in its requirements for both electric energy and cooling water.

The system proposed by the author seems rather complicated in relation to small homes, although it is recognized that the equipment is intended to serve functions other than space heating and cooling. In the heat-storage elements, the equipment might be simplified by storing the hot water at a temperature of 200 to 250. Although this would require greater storage capacity than the high-pressure system proposed, the auxiliaries and the thickness of insulation would be reduced, and it is possible that no greater space would be required and that a lower first cost might be attained. In addition, there is a question whether a high-pressure storage system would be permissible under existing codes; a low-pressure hot-water system would certainly avoid this problem. Also, any problems involving corrosion due to oxygen would be reduced by operating at a lower temperature.

George H. Krueger: In response to discussions by R. R. Herrmann and S. J. Levine, it is the desire of the author to stimulate thought on the expanded possibilities that could be made available to the customer by the utilities, and at the same time permit the utility to control more directly the diversity on its system instead of letting the consumer be the principal controlling factor. The argument was presented in the past that water heaters and ranges would upset the balance on the systems, but it has worked out that both of these items have stimulated greater use of electric energy and a greater desire by the public to make fuller use of the benefits of electric energy.

The author is mindful of the fact that the type of electric heating described in his paper is more applicable in areas where low-cost hydro-generated electric energy is available, rather than in areas where electric energy is generated by fuel-fired plants. In the latter case, heat energy is converted to electric energy and then reconverted to heat energy with all of the conversion losses involved.

Future developments resulting from extended tests may dictate a higher capacity for the individual installation than was originally installed, but the author preferred to start with a minimum design rather than overdesign at the beginning.

The idea of building a summer cooling load to flatten out the annual load curve, in conjunction with a winter heating load, has been taken into consideration by the use of a water refrigerating system for summer cooling purposes. With this type of a system, as shown in Figure 1 of the paper the full advantage of off-peak interruptible service is retained.

Regarding the code requirements of the cities, the author has checked these items throughout the Pacific Northwest, and the proposed system meets all requirements of safety, as well as existing insurance requirements.

The use of a moderate pressure for the system is superior to hot water at low pressures and temperature for the reason that the rate of heat release and heat transfer is very rapid. There is no lag in the system due to the movement of large quantities of low-temperature hot water.

During the coming heating season, full operating data will be obtained and made available to those interested.

Voltage Transients in Arc-Furnace Circuits

Discussion and closure of paper 44-99 by the AIEE subcommittee on transient voltages in arc-furnace circuits of the AIEE committee on electrochemistry and electrometallurgy, presented at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944, and published in AIEE TRANSACTIONS, 1944, August section, pages 563–8.

E. A. Childerhose (Jackson and Moreland, Boston, Mass.): We have been investigating overvoltages at a plant using a large number of electric arc furnaces varying in size from 1,000 kva to 10,000 kva. The number and magnitude of surges closely parallels the arc-furnace transient curve shown in Figure 7 of the paper except that the maximum voltage does not exceed seven times normal.

During staged tests using a klydonograph for recording surges, it was found that all surges were caused by circuit-breaker operations. Breaker closings caused a preponderance of the surges and also the highest surges. It was found also that most of the surges were on the line side of the breaker.

These switching surges were quickly dissipated in the cable feeding this part of the plant. No klydonograph evidence was obtained of their existence a couple of hundred feet away from the bus where they were occurring.

Very little of the literature read in connection with our study mentioned serious voltage surges on the line side of the breakers. All voltage-recovery studies and tests appear to have been made of voltages across the breaker when interrupting load. It is suggested that further studies be made of the recovery voltages measuring the voltage to ground on each side of the breaker as well as voltages across the breaker itself in the hope that this will shed further light on voltage conditions existing in the various parts of the circuit during the making and breaking of currents.

Consideration should be given also to the possibility of reducing these switching surges by switching under load rather than no-load conditions as is the present common practice. The advantage of reduced voltage surges would have to be balanced against the disadvantage of increased wear and tear on the breaker.

H. A. Peterson (General Electric Company, Schenectady, N. Y.): This report is tangible evidence that progress is being made toward the goal of understanding the problem of and protecting against transient overvoltages associated with arc-furnace circuits.

The summary included in this report is brief but significant. One of the most significant facts brought out is that overvoltages of greatest magnitude appear to be associated with breaker-opening operations. Overvoltages of lesser magnitude are sometimes encountered during closing. It appears that overvoltages occurring during the melt-down period and not associated with breaker operations are localized pretty well on the low-voltage winding, or at least do not appear to any serious extent at the high-voltage terminals of the transformer.

There is no reason to think of the switching of an arc-furnace transformer as being different from the switching of any power transformer as far as the circuit itself is concerned. The magnitude of switching overvoltages associated with arc-furnace transformer switching is about the same as for general system switching. However, there is a fundamental difference between these cases from the standpoint of the frequency of switching. Arc furnaces generally are switched many times each day, and consequently, transient overvoltages are experienced much more frequently in the neighborhood of the arc-furnace transformer than in the vicinity of most transformers used for supplying other types of loads. Also, because of this frequent operation, breakers used with arc furnaces are subjected to unusually severe mechanical duty.

In view of this frequency of occurrence of overvoltages in many arc-furnace installations, the question of the need for overvoltage protection arises. Lightning arresters afford one means of putting a ceiling on such transient overvoltages. To afford best protection to the transformer, arresters used for this purpose should be installed on the transformer side of the breaker. Voltages in excess of about four times normal line-to-neutral crest should be eliminated effectively by arresters at the arrester location regardless of their nature or origin. A study of the results shown in Table III of the paper appears to substantiate this reasoning.

If the "inductive kick" voltages accompanying transformer magnetizing-current interruption is the chief source of difficulty, then shunt capacitors of the proper value placed at the transformer terminals theoretically should afford an ideal corrective means. If the capacitor kilovolt-amperes equal the transformer magnetizing kilovolt-amperes, no voltages of this type are possible, thus affording an ideal solution. However, in most installations transformers may operate over a range of taps and possibly with several different winding connections, thus varying the transformer magnetizing kilovolt-amperes over quite a range. Fortunately, the capacitor does not lose its effectiveness at any critical value and, consequently, is expected to be superior to the arrester as a corrective means for this particular type of overvoltage.

In view of these considerations, shunt capacitors and arresters located at the high-voltage terminals of the arc-furnace transformer appear to offer the best overvoltage protection at the present time. Such an arrangement places a definite ceiling on the overvoltages associated with the furnace installation. Another possible method of reducing the switching surge voltages is by the use of a breaker of the magneblast type which inserts impedance into the arc path during interruption in such a way that the recovery voltage oscillations are highly damped and essentially independent of circuit constants.

J. E. Hobson: The discussion of Mr. Childerhose covering tests made on arc furnaces substantiates in several respects data given in the paper. In most of the tests made by the committee it was found that the majority of surges appearing on the high side of the transformer and the higher magnitude surges were associated with circuit-breaker operations. This would tend to indicate that the higher surges appearing on the transformer's primary winding come from the phenomena of arc interruption in the primary breaker rather than from arcing phenomena in the furnace. There is ample evidence to indicate that high voltages are generated by the furnace arc, but most of those voltages are dissipated in traveling through the transformer and do not appear with high magnitudes on the primary.

I do not believe that Mr. Childerhose has drawn a correct inference regarding the location of voltage measurement in most of the tests reported in this paper. In practically every case the voltages recorded are voltages between line and ground rather than across the breaker.

I am glad to have the comments of Mr. Peterson and I agree entirely with his views. Shunt capacitors have been proved both in the field and in the laboratory to be effective devices for reducing voltage transients appearing at the primary of arc-furnace installations. Field tests also have proved that Mr. Peterson's comment regarding size of capacitor is correct: over a considerable range the capacitor size is relatively unimportant in determining the transient voltages which can appear. The shunt capacitor is relatively more effective than a lightning arrester, since all voltages are reduced materially instead of establishing a ceiling voltage for them.

It might be well to emphasize again what seems to be the conclusion of most investigators: that transient voltages occurring on the primary side of arc-furnace installations do not differ materially in magnitude from usual switching surges occurring on a system, but the frequency of occurrence is many times greater. Since some insulation associated with the primary circuit is seriously affected by large numbers of overvoltages, arc-furnace circuits do merit some special consideration in an attempt to lower the magnitude of surges and limit the number of surges occurring.

Through an oversight complete information regarding the test data of plant A, referred to in Figures 2, 3, 4, 5, 6, and 7 and in Tables I, II, III, and IV of the paper was not made available to the data group responsible for the assembly of the report. In view of conditions existing during the test and thereby affecting the test results, it appears desirable to withdraw the results of the test at plant A from further consideration in the study of the complete report.

The engineer responsible for the tests at plant A submitted the following explanation for the abnormally high voltage obtained:

"While we were making these tests, we found by a recording device on the low-voltage side of the furnace, that we were getting an extremely large number of surges during one portion of the operation. On investigation, we found that these surges occurred at a time when there was a loose shunt on one of the breakers forming the delta connection for the high-voltage circuit. At this installation the star-delta connections were made by means of circuit breakers.

"We found that, after the connections in the delta breaker were secured properly, the large number of surges indicated were entirely absent, and it is quite possible that the very high surge voltages on the high-voltage side indicated independently by the klydonograph during this period were caused by the same loose connection which gave rise to a large number of surges.

"Since there is some question as to the source of these particular high voltages on this test, it might be well either to omit them altogether from your summary, or else to give an explanation for the possibility of abnormal conditions causing these high voltages."

Induction Heating—Selection of Frequency

Discussion and author's closure of paper 44-166 by N. R. Stansel, presented at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944, and published in AIEE TRANSACTIONS, 1944, October section, pages 755–9.

T. H. Long (C. G. Conn, Ltd., Elkhart, Ind.): There seems to be a minor error in the result stated in Figure 5 of the paper dealing with the proportion of the total heat energy released in V_p. The trouble starts with equation 4 in which the fraction in Bessel functions gives the relation between maximum current densities at the points x and a rather than an integration of the heating effects to those points.

The desired relation may be derived by setting

W_Δ = power input in watts per centimeter of axial length of charge

$W_{(\Delta-1)}$ = power input in watts per centimeter of axial length of core of radius $x=a-p$

Then

$$W_\Delta=\frac{H^2\rho}{0.16\pi}\Delta\left\{\frac{\operatorname{ber}\Delta\operatorname{ber}'\Delta+\operatorname{bei}\Delta\operatorname{bei}'\Delta}{\operatorname{ber}^2\Delta+\operatorname{bei}^2\Delta}\right\}\tag{1}$$

$$W_{(\Delta-1)}=\frac{H^2\rho}{0.16\pi}(\Delta-1)\times\left\{\frac{\operatorname{ber}(\Delta-1)\operatorname{ber}'(\Delta-1)+\operatorname{bei}(\Delta-1)\operatorname{bei}'(\Delta-1)}{\operatorname{ber}^2\Delta+\operatorname{bei}^2\Delta}\right\}\tag{2}$$

The proportion of the total heat energy released in V_p then is

$$\frac{W_\Delta-W_{(\Delta-1)}}{W_\Delta}=1-\frac{\Delta-1}{\Delta}\times\left\{\frac{\operatorname{ber}(\Delta-1)\operatorname{ber}'(\Delta-1)+\operatorname{bei}(\Delta-1)\operatorname{bei}'(\Delta-1)}{\operatorname{ber}\Delta\operatorname{ber}'\Delta+\operatorname{bei}\Delta\operatorname{bei}'\Delta}\right\}\tag{3}$$

When Δ is large this may be approximated by

$$1-\frac{\Delta-1}{\Delta}\times\left\{\frac{\sinh[(\Delta-1)\sqrt{2}]-\sin[(\Delta-1)\sqrt{2}]}{\sinh(\Delta\sqrt{2})-\sin(\Delta\sqrt{2})}\right\}\approx1-\epsilon^{-\sqrt{2}}=0.757\tag{4}$$

For values of Δ	=	2	3	4	5	6	∞
Proportions	=	0.94	0.84	0.76	0.76	0.76	0.757

Secondary Reactance

Referring to the quantity defined by equation 27, as reactance is confusing, since this quantity is related directly to neither flux linkages per ampere nor to power factor. The complete expression for the reactance of the coil and charge should be, following equation 29,

$$X=\frac{8\pi^2fN^2}{l10^9}\left[b^2k_b-a^2k_a'Y^{1/2}\left(1-\frac{2\theta}{\Delta}\right)\right]\tag{5}$$

so that evidently equation 27 is a measure of the screening effect of the charge on the reactive component of flux. It is worth noticing that what might be called the intrinsic power factor of the charge is always 0.707, when the frequency is high, $\Delta>10$.

The ideas of reactance and resistance of the charge are tied up with the equivalent circuit of the transforming system involved. They are very useful as analytical devices, but as such they have limitations. A somewhat more direct analysis avoids these limitations but otherwise is not especially useful.

Voltage Considerations

The effect of the per-turn voltage in determining the frequency for any particular induction-heating job has been touched only lightly. Occasionally this will be the determining factor in design. For a given power input to the charge, and for $\Delta > 10$ it can be shown that the voltage due to the leakage flux is proportional to $f^{3/4}$ and that component of voltage due to flux through the charge is proportional to $f^{1/4}$. If subscripts a and l refer to charge and leakage respectively, then

$$r_a=k_1f^{1/2}\tag{6}$$

and for constant energy input to charge

$$E_a=k_2r_a^{1/2}=k_3f^{1/4}\tag{7}$$

$$I=k_4r_a^{-1/2}=k_5f^{-1/4}=k_6H\tag{8}$$

$$\text{var}_l=k_7H^2f=k_8f^{1/2};\quad E_l=\text{var}_l/I=k_9f^{1/2}/f^{-1/4}=k_9f^{3/4}\tag{9}$$

Stirring Action in Melting Service

In referring to the stirring action as proportional to the square of the ampere turns, no account has been taken of the phase differences between current and field densities that are of substantial importance for values of the electrical dimension $\Delta < 4$.

An expression for the integrated magnetic pressure at the axis of the charge in pounds per square inch is

$$\text{Pressure}=0.695\times10^{-9}B^2S_\Delta/\mu\tag{10}$$

where

B is the air-gap flux density in lines per square inch

μ is a fictitious permeability less than 1 to correct for end effect, approximated from Nagoaka's constant

and

$$S_\Delta=1-\frac{1}{\operatorname{ber}^2\Delta+\operatorname{bei}^2\Delta}$$

Having values	0.028	0.337	0.737	0.915	0.974	0.992
For Δ =	1	2	3	4	5	6

This means for instance that, if a power input be selected so that the magnetic pressure at the axis of the charge is one pound per square inch when $\Delta=2.5$ and then the frequency and voltage are so varied as to keep the power input to the charge constant, the magnetic pressure at the axis will vary as follows:

Δ	=	1	2	3	4	5	6
Pounds per square inch	=	1.08	1.03	0.95	0.82	0.67	0.56

Another consideration worth notice is the fact that the stirring varies appreciably with the level of the molten metal, being less when the metal level is a few inches above the top of the coil and increasing as the metal level is lowered.

N. R. Stansel: The statement in the paper that the shape of the charge is taken into account in analyses of the heating effect of eddy currents is illustrated by the example of a solid plate which is exposed on both sides to an alternating magnetic flux, the directions of which are parallel to the sides of the plate.

The index ratio of the plate is

$$\nabla=\frac{\sqrt{2}c}{p}\tag{1}$$

c = one half the thickness of the plate, centimeters

The equation for the rate of heat development in the plate which corresponds to equation 3 of the paper for the solid cylinder is

$$P_{em}^2=\left(\frac{1}{16\pi}\right)(H^2)\left(\frac{\sqrt{\mu f}\sqrt{\rho10^9}}{c}\right)\times[G]\times10^{-7}\text{ watts per cubic centimeter}\tag{2}$$

$$[G]=\left[\frac{\sinh\nabla-\sin\nabla}{\cosh\nabla+\cos\nabla}\right]$$

If one half the thickness of a plate is equal to the radius of a solid cylinder divided by $\sqrt{2}$, that is, $c=\frac{a}{\sqrt{2}}$ the numerical values of the index ratios of the two shapes are the same, that is, $\Delta=\nabla$ and

$$\frac{P_{em}^2\text{ plate}}{P_{em}^2\text{ cylinder}}=\frac{[G]}{2[Fr]}\rightarrow\frac{1}{\sqrt{2}}\tag{3}$$

For $\Delta=\nabla=3.0$

$$\frac{P_{em}^2\text{ plate}}{P_{em}^2\text{ cylinder}}=1.0\tag{4}$$

A comparison of the rates of heat development in a solid cylinder and in a plate can be made by taking $c=\frac{a}{\sqrt{2}}$. The variable in equations 3 of the paper and 2 of this closure can be either resistivity or frequency. Taking resistively as a variable—and the only variable—in these equations, the comparison on that basis is shown in Figure 1.

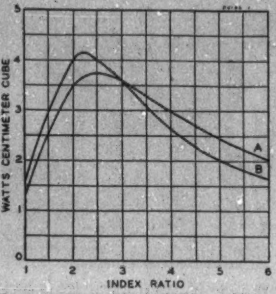

Figure 1. Comparative rates of heat development in solid cylinders and plates with resistivity as the variable

A—Solid cylinder
B—Plate

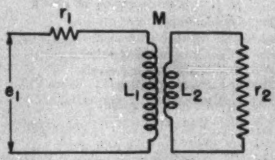

Figure 2. Circuit diagram of induction-heating coil and load

Mr. Long's equation 3 for the proportion of the total heat released in volume V_p is correct for the same temperature value at all points in the mass of the charge.

Equation 5, in Mr. Long's discussion is the same as equation 29 in the paper.

The circuits of the transformer of Figure 1 of the paper are represented by the diagram of Figure 2 of this closure. The secondary circuit with resistance r_2, inductance L_2, and current i_2 is coupled to the primary circuit, which has resistance r_1, inductance L_1, and current i_1 by a mutual inductance, M.

The equations of these circuits are

$$(r_1+j\omega L_1)i_1+j\omega Mi_2=e_1 \tag{5}$$

$$(r_2+j\omega L_2)i_2+j\omega Mi_1=0 \tag{6}$$

$\omega=2\pi f$ $e_1=$applied voltage

Substituting the value of i_2 from equation 6 in equation 5 and placing

$$k=\frac{\omega^2M^2}{r_2^2+\omega^2L_2^2}$$

one reduces these equations to

$$[(r_1+kr_2)+j\omega(L_1-kL_2)]i_1=e_1 \tag{7}$$

Thus the load of the secondary circuit is equivalent to a resistance kr_2 and a negative reactance ωkL_2 placed in the primary circuit.

Generally the index ratio in melting service is much higher than the value 4 noted in Mr. Long's comments on the stirring action.

Characteristics of Chlorinated Impregnants in D-C Paper Capacitors

Discussion and authors' closure of paper 44-165 by L. J. Berberich, C. V. Fields, and R. E. Marbury, presented at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944, and published in AIEE TRANSACTIONS, 1944, pages 1173–9.

F. J. Given and A. J. Christopher (Bell Telephone Laboratories, Inc., New York, N. Y.): This paper is gratifying evidence of the growing interest in the specialized problem of design of capacitors for operation on d-c potentials. There has been a scarcity of information on methods of predicting operating life under stated conditions of voltage and temperature. The present paper serves a useful purpose in adding design data.

The use of chlorinated materials, particularly chlorinated diphenyl has been for many years a subject of special interest in the Bell Telephone Laboratories. At first, disappointing results were obtained with it caused by its relatively short life at high temperatures under the usual potential gradients. Research studies in Bell Laboratories, however (reference 9 of the paper) diagnosed the trouble, and it is interesting that the authors of the present paper found the same phenomena and propose the same remedy.

In comparing their results with those in our laboratories two points merit attention. They report decreasing insulation resistance with increasing potential gradient (Figure 10) on capacitors both with and without inhibitors. This fact together with the comparatively low values of insulation resistance at the higher potential gradients (500 to 1,000 volts per mil) suggest the presence of impurities in either the paper or impregnants. According to our experience, the presence of small amounts of water or of foreign substances soluble in the impregnant could account for this behavior. Our results with the more common designs rated at 600- to 2,000-volt direct current show it is possible to obtain commercially two to four times the resistance values shown in Figure 10 for the uninhibited impregnant at the higher gradients. Furthermore, it is also possible to attain substantially the same high values for capacitors using the inhibitor material. In other words, the inhibitor can be added without producing any appreciable degradation in insulation resistance.

As a result of obtaining high insulation resistance at the higher gradients, the heating problem mentioned in this paper has not been dominant in affecting life performance. Accordingly, it has been possible to make accelerated life tests at higher than rated potentials at high temperatures (85–95 degrees centigrade) on actual samples of the product. It has not been necessary to employ the special test samples described in the paper.

Louis Kahn (Aerovox Corporation, New Bedford, Mass.): The authors have presented for the first time characteristics of capacitors that generally have been known to a few workers in a highly specialized field.

From an application point of view, it is of interest to note that capacitors operated at elevated temperatures, above 65 degrees centigrade, have a definite life. Also characteristics that have been considered as constant are shown to be dependent on temperature and frequency. Knowledge of these factors should enable the application engineer to design equipment, using capacitors, to have a longer service life and greater continuity of service.

The life test stresses used, 1,000 volts per mil, are lower than the stresses used heretofore at room temperatures (Signal Corps and Navy capacitor specifications). However, operation at 85 degrees centigrade requires a decrease in dielectric stress.

In considering the life-test data with reference to capacitors, it should be remembered that the capacitance of the test samples was approximately 0.25 microfarad. On commercial units of higher capacitance and voltage rating, the life to be expected will be less than the figures indicated in the paper, if the same stresses and temperatures are used.

Moreover, the test samples were purposely made so that the temperature of the capacitor section under test was held constant. On the larger units the case temperature is not the section temperature nor is it the hot-spot temperature, which last is the determining factor in the life of the capacitor. This is especially important to remember on capacitors subjected to alternating or to high direct voltages.

L. J. Berberich, C. V. Fields, and R. E. Marbury: We wish to thank F. J. Given and A. J. Christopher for their interesting comments. The excellent fundamental work carried out at the Bell Telephone Laboratories has been inspiring to all workers in the field of dielectrics.

Their discussion raises a question as to the magnitude of the resistance values reported in the paper. The 85 degrees centigrade values plotted in Figure 10 of the paper are approximately five times higher than those required by the latest American Standards Association specifications. These data were determined on a group of capacitors taken from regular production. It is true that some units can be found that have twice the values reported in the paper. However, we have not obtained a longer life in the case of these capacitors. Curves *C* of Figure 12 in the paper show that an extremely long life can be obtained, despite the fact that the resistance is not of a very high order. This point bears further investigation.

Mr. Given and Mr. Christopher point out that they have not found it necessary to employ an especially constructed test sample such as the one described in the paper. We agree with them that thermal instability is no problem in testing the smaller commercial units. The special test samples were designed solely for the laboratory where all kinds of new impregnants and stabilizers are investigated. This eliminates the additional variable of thermal instability in those cases in a comprehensive laboratory investigation where materials may be investigated that do not always result in a capacitor having high resistance.

The possibility of thermal instability arising in testing the smaller commercial units was perhaps overemphasized. However, in testing large units, perhaps larger than Mr. Given and Mr. Christopher have had occasion to test, the special air-circulation procedure also described in the paper is necessary to prevent thermal instability at high ambient temperatures and voltage stresses.

Mr. Louis Kahn states that the life to be expected from the larger commercial units having higher capacitance ratings should be less than that for the special 0.25-microfarad unit used in many of our tests. This has been recognized by those formulating the American Standard Association specification, and allowance has been made for this fact by derating the larger size units.

The Copper-Oxide Rectifier in Electrochemical Work

Discussion of paper 44-163 by I. R. Smith, presented at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944, and published in AIEE TRANSACTIONS, 1944, October section, pages 739–44.

E. A. Harty (General Electric Company, West Lynn, Mass.): The author's review of the field of application of copper-oxide rectifiers to electrochemical work seems to parallel our own experience.

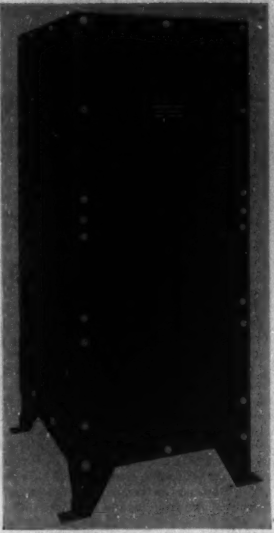

Figure 2. General Electric copper-oxide rectifier, type *RC-141-F*, for electroplating and anodizing. Twelve-kilowatt output. Oblique front view (from left)

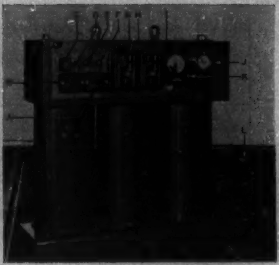

Figure 3. General Electric air-cooled induction-voltage regulator for automatic-anodizing installation. Oblique front view

- *A*—Transfer relay
- *B*—Motor-control relay
- *C*—Low-voltage relay
- *D*—Regulator motor on–off switch
- *E*—Current adjustment
- *F*—Voltage adjustment
- *G*—Contact-making voltmeter
- *H*—Voltage-sensitive relay
- *I*—One-hour timer
- *J*—Position indicator
- *K*—Time adjustment
- *L*—Blower

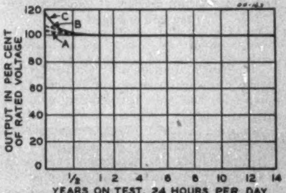

Figure 4. Stability of General Electric copper-oxide rectifiers

- *A*—Fan-cooled rectifier (room temperature)
- *B*—Standard natural cooling
- *C*—Hot rectifier operating at 150 degrees to 170 degrees Fahrenheit

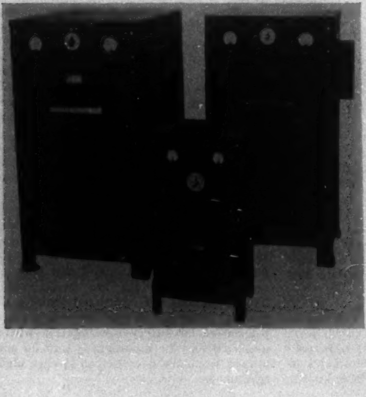

Figure 1. Three General Electric copper-oxide battery chargers for electric truck batteries

Thousands of low-rate battery chargers are in daily use by railway, radio, signal, telegraph, and telephone engineers in circuits where batteries must be used to maintain continuity of service during periods when the power supply fails. Battery charging at higher rates has also been quite popular and designs are available to charge batteries up to 300 amperes. Typical examples of this type are shown in Figure 1. Electroplating applications have been very numerous and designs have been made to cover ranges from a few amperes up to 120,000. A typical 2,000-ampere unit is shown in Figure 2. One of our most recent developments is a fully automatic system to anodize aluminum. The tank rating is 42 volts at 3,000 amperes and the electric system consists of a fully automatic voltage-current controlled induction-voltage regulator, as shown in Figure 3, supplying power to a group of ten rectifiers, each rated 42 volts at 300 amperes. All the operator does is load the tank, push a button, and then forget the whole thing until a gong calls him back to duty. The "build up" proceeds at rated direct current and a varying voltage until 40 volts are reached. Automatic transfer takes place and constant voltage is now held instead of constant current. Anodizing proceeds for a presettable time of usually 35 minutes. At the end of this cycle power is released, the regulator resets to the starting point in readiness for the next cycle, and the warning gong rings, thus notifying the operator that it is time to unload and reload the tank.

Finally, it is interesting to note how closely typical life tests agree, indicating the extremely long life of the copper-oxide rectifier as shown in Figure 4.

Extending the Use of Shunt Capacitors by Means of Automatic Switching

Discussion and author's closure of paper 44-178 by W. H. Cuttino, presented at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944, and published in AIEE TRANSACTIONS, 1944, September section, pages 674–8.

B. M. Jones and C. N. Clark (Duquesne Light Company, Pittsburgh, Pa.): In past years the applications of shunt capacitors for improvement of voltage on distribution circuits have been limited generally to those locations where relatively small increases in voltage were required. In cases where a large increase in voltage was needed, it was necessary to utilize other methods such as step regulators, extending additional phase wires, or the use of larger conductors.

The use of automatic switching in conjunction with shunt capacitors, as described in Mr. Cuttino's paper, will permit shunt-capacitor installations in cases where the uncontrolled capacitor could not have been applied previously on account of high voltage encountered during the light-load period.

The author's method of calculating voltage drops and rises by combining the products of the kilowatt load in amperes times the circuit resistance in ohms with the kilovar load in amperes times the circuit reactance in ohms has been used for a good many years by the Duquesne Light Company engineers in order to simplify calculations and save time in various engineering studies. For the general run of problems of this sort, the accuracy is within reasonable limits, even though it is based upon an approximate formula for voltage regulation.

Two customers on our system within the past 10 years or so have experienced difficulty, damage, and expense due to the lack of automatic switching to take their capacitors off the line under light-load conditions.

In one case, a large department store with an appreciable power load installed static capacitors for power-factor correction, and after store hours a considerable portion of the motor load was shut down. The resultant voltage on the remaining loads gradually crept up until it was sufficiently high to break down the units in the static-capacitor bank. This automatically corrected itself by burning out the "weak" capacitor units, one after the other, until there was only sufficient static capacitor capacity left to bring the light-load voltage up to a value which the remaining capacitor units could withstand—not an engineering solution by any means! Several months elapsed before this was discovered, and then it is believed the improved type of capacitor was provided. It is not thought that any automatic switching was provided, although it is understood that instructions were issued to switch fuses under certain conditions of store operation and motor load.

In another case, at a small industrial plant the shutting down of the motors caused the voltage to rise due to the capacity effect until about noon or shortly thereafter when practically all of the motors shut down, and a fifth harmonic was produced which blew the 4-kv fuses on our incoming supply line. This was investigated by us and the customer (who did not have the experience needed), and it is believed the capacitors were connected to certain of the motors so that shutting down of the latter took off the capacitors.

In 1930 and 1931, the Duquesne Light Company made 19 installations of three-phase shunt capacitors totaling 1,680 kva on four 4-kv distribution circuits located in industrial areas and serving mostly power loads. These capacitors originally were installed for the purpose of improving the power factor and releasing capacity for other loads. A number of the larger customers later installed capacitors to reduce their kva demands and take advantage of the savings permitted by the power-factor clause in the rates. This change, combined with restricted growth during the depression, made it possible to release most of the capacitors for use at other locations to improve voltage. By the end of 1943 there were a total of 30 installations of shunt capacitors on distribution circuits with a total capacity of 2,550 kva. All of these installations were connected to the line 24 hours a day, and in no case was the voltage rise large enough to cause excessive voltage at light-load periods.

We believe that the development of automatic switching equipment for shunt capacitors will permit the use of shunt capacitors in many cases where they would be impractical when uncontrolled. Although there are undoubtedly a number of cases where the current or kilovar control can be applied successfully, it is believed that the voltage control will find more general use. With the availability of automatic switching equipment the choice between automatically controlled capacitors and other methods will be made, after careful consideration of operating advantages and disadvantages and the over-all initial and annual costs involved in each case.

The application of capacitors and their widespread use as described by Mr. Cuttino strikes a responsive chord in one of us (Mr. Jones), as he was involved in the installation and testing of early static capacitors in 1922 in a small hydroelectric plant at Three Rivers, Mich., near Constantine, Mich. This testing and field investigation was carried on jointly by R. E. Marbury of the Westinghouse Electric and Manufacturing Company and Mr. Jones, and there was no automatic voltage control used, as this was an attended hydroelectric plant where switches could be operated manually as required, although to the best of his recollection capacitors were in service continuously.

W. C. Bloomquist (General Electric Company, Schenectady, N. Y.): The author is to be commended for the examples illustrating the step-by-step procedure for selecting the size of the capacitor bank when automatic switching is used. Such examples are especially helpful to those who do not have an everyday acquaintance with capacitor applications.

This paper makes no reference to a power-factor control device. In many cases the choice between a power factor and kilovar master element requires consideration. There are application limitations of the power-factor control which might have been included.

I would like to point out a big difference between the utility and industrial application for the selection of a suitable capacitor control to reduce overvoltage. Many of the industrial plants require some type of single-step control to remove capacitors at light load due to high-voltage conditions. Offhand, a voltage control would seem to be the logical selection, but strange as it may seem at first thought, a *kilovar* (and sometimes a current control) and not a voltage control is the best selection, and for this reason: The voltage variation in the industrial plant is made up of the variation in the incoming supply source, plus the variation within the plant. Since the supply voltage may vary independently of the load of the industrial plant, the supply voltage might be high at the time of maximum or high load at the industrial, and therefore the capacitors would be switched out of service just when they are needed most.

Mention has been made of the method of switching capacitors with the motor and the problem of overvoltage due to self excitation, if too large a capacitor is used. In addition to this, too large capacitor banks at motor terminals may cause excessive transient torques resulting in damaged couplings and twisted shafts on switching. This effect is most pronounced on motors with reduced-voltage starters, and the reason for it is this: At the moment of transfer from reduced voltage to full voltage, the motor acts as a generator, due to the excitation supplied by the capacitor, and the voltage and phase relations may differ considerably from the line conditions. This also applies to full-voltage starters where operation may require the starter to be opened and then immediately closed.

To avoid these excessive transient torques and to reduce the motor overvoltage to a reasonable value, we have found that a sensible application rule to follow is "apply capacitors to the extent of improving the full-load motor power factor to approximately 95 per cent."

W. H. Cuttino: Capacitors have been switched automatically in response to a power-factor relay as proposed in Mr. Bloomquist's discussion. In such a case it is necessary to divide them into very fine increments or provide a power-factor relay with special features to prevent hunting. These special features may consist of arrangements to reduce the sensitivity of the relay with reduction in load, recalibrate with switching operations or a bias which makes it possible to adjust the relay to switch the capacitors within certain power-factor limits plus a constant kilovar spread to prevent hunting. Because of the limitations and special features, other more generally accepted methods of automatic control are usually preferable.

Current control is simpler and less expensive than kilovar control, and less instrument transformers are required. On circuits where the power factor remains relatively constant with variation in kilowatt loading, current control is often preferred for initiating the switching of capacitors to reduce kilovar demand. If more than one switching step is desired, a separate current control is required for each capacitor switch. Contrary to Mr. Bloomquist's opinion, practical applications have been encountered and installations with separate current control for each step have been in successful operation for several years.

Automatic Control and Switching Equipment for Capacitor Banks and Its Application

Discussion and authors' closure of paper 44-173 by T. W. Schroeder and W. C. Bloomquist, presented at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944, and published in AIEE TRANSACTIONS, 1944, September section, pages 649–54.

V. E. Hill (Duquesne Light Company, Pittsburgh, Pa.): It is true that static capacitors are being used increasingly by both utilities and industrial and commercial establishments for improvement of voltage, gaining of additional equipment or circuit capacities, and reduction of bills. Also, it probably would be desirable to have automatic control on many of these applications, particularly those made by the utilities. However, there are various factors involved as brought out in the following discussion.

Applications on the Utility System

The Duquesne Light Company at present has only about 30 installations totaling 2,550 kva of static capacitors connected to its four-kilovolt distribution circuits. All these are connected permanently (except, of course, for maintenance or repairs) and are used exclusively for voltage improvement.

Since our circuit and substation power factors generally run from 90 to 95 per cent, it readily will be seen why installations of static capacitors have not been made to gain system capacity or for general reactive supply.

We do have some need for the application of more capacitors for voltage control, but they have not been installed owing, among other things, to the lack of a suitable automatic control. By "suitable" we mean a compact unit for pole mounting that is also fairly economical. If and when such a device is made available, we probably will install many more capacitors. As previously mentioned, the control equipment desired would be based on voltage-sensitive devices as described by the authors. Of course, the war situation threw this and everything else out of gear. We are developing a design of assembled equipment for pole mounting, which will be available shortly.

Customers' Installations

There are many capacitor installations on our customers' premises. No complete accurate data are available, but the known installations connected between 1929 and 1942 inclusive numbered over 600 with a total capacity of approximately 28,000 kva. These installations vary in size from a few kilovolt-amperes up to several hundred kilovolt-amperes, with a few installations exceeding 1,000 kva.

Probably 90 per cent or more of these capacitors are connected permanently and are never switched except, of course, for trouble or maintenance. Furthermore, most of them are directly connected to their main busses, although a few are installed at or near the ends of the plant circuits, primarily to gain circuit capacity and only incidently for voltage improvement.

The vast majority are installed for power-factor improvement and consequent saving in rates due to the fact that our billing demands are on a straight kilovolt-ampere basis. Reactive meters are ratcheted so that there is neither a penalty nor a bonus for leading power factor. As a consequence of the capacitors being permanently connected, customers either are not troubled or not greatly concerned with high voltages or extra losses during light-load periods. Connected directly to the main busses, voltages are not normally appreciably higher during light-load periods. Probably most of the capacitors which are switched are those associated with lower power-factor devices such as welders, induction motors, and the like.

It appears as though customers are wary of switching capacitors manually when not needed because of the possibility of forgetting to reconnect them when needed and thus reducing their monthly power factors and increasing their bills. We recall one such case several years ago, in which the capacitors gave rise to a resonant condition which greatly increased the fifth harmonic currents causing fuse trouble and overheating during light-load week-end periods. The customer was exceedingly reluctant to switch these on and off on week ends but finally did so, because it was the most expedient solution.

Thus, it appears as though most of our customers come under item 4 mentioned by Schroeder and Bloomquist, wherein capacitors are applied for rate purposes only, and voltage rise and losses at light load are not a problem. As also mentioned by the authors, where it is desired to have the capacitors on at heavy loads for rate reasons but off at light load to minimize voltage rise or losses, the kilovar or power-factor master element will provide the most direct solution of the control problem.

J. G. Holm (Boston Port of Embarkation, Boston, Mass.): I should like to ask the authors of this paper how the automatic control and switching equipment they described would perform under intermittent loads and frequent operations. Would it be suitable for such operation, or would it have to be modified?

W. C. Bloomquist: When the equipment must operate repetitively the switching device should be a contactor, as it is designed for that type of duty.

If Mr. Holm has reference to welder or similar loads of intermittent and frequent operation, then the equipment described in this paper should not be applied. In those cases lighting flicker is the main problem and is taken care of adequately by a series capacitor, either as part of the welder unit or connected in the welder feeder-supply circuit, but in either case the series capacitor is not switched. When the series capacitor is part of the welder unit it improves the power factor and reduces the kilovolt-amperes drawn from the power supply and the voltage drop. When connected in the welder-supply feeder circuit, it neutralizes the reactance—usually the largest part of the voltage drop—leaving only the resistance component, which generally is small in welder-feeder circuits. Either series capacitor application accomplishes the ultimate result of improved voltage.

Progress in Impulse Testing of Transformers

Discussion and author's closure of paper 44-169 by J. H. Hagenguth, presented at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944, and published in AIEE TRANSACTIONS, 1944, pages 999–1005.

R. E. Pierce (Ebasco Services Inc., New York, N. Y.): The author is to be complimented for bringing before the Institute for discussion certain problems in connection with the impulse testing of transformers, as well as for development of a new supplementary testing technique having promise of more positive fault detection.

The fundamental question often has been discussed but not finally answered, that is, whether routine impulse testing of all large power transformers is justified or desirable. The reason for the question is the belief on the part of some that there may be a possibility of damaging insulation during the impulse test without being able always to detect such damage.

Even though possibilities may exist of test failures going undetected with the state of the art as it has been to date, it is the opinion of this discusser and his associates, based on a considerable experience in application and use of power transformers, that routine impulse testing of all power transformers would be desirable. It has been our practice to specify carefully and witness impulse testing with careful observation of all possible indicators of distress for failure for the majority of power transformers purchased.

It is our further opinion that, if the arrangements for impulse testing in the factories could be systematized more nearly as for other routine tests and the cost of impulse testing thereby reduced, it would be justified and desirable as a routine test on all power transformers.

Our experience with impulse testing of power transformers has shown that where a transformer has failed during test, failure almost always has been caused by faulty material or workmanship or lack of adequate engineering covering minor assembly and connection details, rather than from improper fundamental design. No instance is recalled during the ten years or so since impulse testing was begun, where a transformer which has without question passed all specified impulse tests, including a steep-wave-front test, subsequently has failed in service from lightning. The only cases recalled of failures in service of transformers which have been impulse tested were where during the test there was recognized what appeared to be some irregularity in the test observations but which was not repeated or otherwise conclusively demonstrated to be negligible.

It has been our practice to specify a steep-wave-front test (1,000 kv per microsecond to specified gap flashover) in addition to the reduced and full-wave tests, and the chopped-wave test, as we have believed this more nearly simulates the severe surges met with in service which are most likely to cause failures. We believe that both the steep-wave-front test and the chopped-wave test are desirable, because each places the most severe stresses for that test on slightly different parts of the transformer winding.

It also has been our practice to specify normal-frequency excitation during impulse tests, not primarily because of the stresses created at some points in the winding, but because the magnetic oscillograph furnished one more possible means of detecting a failure. It is admitted, however, that in many cases it has been found desirable to omit the normal-frequency excitation in order to interpret more clearly the cathode-ray oscillograms. Serious consideration well might be given to the omission of the normal-frequency excitation provision.

Correct interpretation of impulse-test oscillograms and procedures is almost an art rather than a science, and we are heartily in accord with the emphasis placed by the author on the importance of proper relationship of circuit constants rather than smoothness of the impulse wave and believe there is a real need for more detailed rules in the test code covering this and related questions of connections in the test setup, such as the relative physical location, length, and size of leads to voltage dividers, test gaps, transformer under test, and surrounding objects.

Reference is made by the author to the desirability of limiting the stress on any winding not being directly tested to 90 per cent of its specified full-wave test voltage. When possible, would it not be more logical to limit the voltage appearing at the terminals of such other windings to the value of the reduced full-wave voltage, namely 60 to 80 per cent of the specified full wave?

E. R. Whitehead (Duquesne Light Company, Pittsburgh, Pa.): Mr. Hagenguth has given an excellent review of transformer impulse-testing technique, and the problems of failure detection which he analyzes are familiar to all those associated with transformer impulse testing.

The writer has conducted, supervised, or witnessed impulse tests on approximately 100 power transformers ranging from 100 kva to 40,000 kva, and on over 3,200 distribution transformers ranging from $1^1/_2$ kva to 200 kva. As a result of this experience dating from 1933, we have drawn much the same conclusion as Mr. Hagenguth; namely, that the 60-cycle excitation is generally more of a hindrance than a help in failure detection during the test.

For several years our purchase specifications for power transformers have modified the standard tests to include a final full wave without power and with all power-isolating gaps short-circuited to minimize the oscillations resulting from their breakdown. In practice this wave immediately followed the full wave with power, since the work of short-circuiting these gaps could be quickly accomplished.

Mr. Hagenguth's new method for failure detection is fundamentally sound, and it should receive careful consideration. I do not believe that the lack of two oscillographs should stand in the way of experimental use of the method, for a single oscillograph can be switched quickly from one source to another for successive impulses.

Finally, I believe serious consideration should be given the proposal to eliminate normal-frequency excitation during the impulse test.

W. C. Sealey (Allis-Chalmers Manufacturing Company, Milwaukee, Wis.): Mr. Hagenguth has presented a very timely discussion on impulse failures in transformers and means for their detection. The 60-cycle excitation during impulse testing is for the purpose of failure detection. It has only a relatively slight effect on the stresses produced in the transformer during testing.

Of the methods of failure detection 60-cycle power follow is, in our experience, the least valuable of the various methods of failure detection, since power follow occurs only in very exceptional cases. Sixty-cycle excitation during impulse testing has the disadvantage of making the test setup more complicated, and, what is equally or more serious, 60-cycle excitation has the effect of reducing the effectiveness of the other means of failure detection. Because the addition of the excitation transformer has the effect of reducing the impedance of the transformer to the surge, stiffer coupling between the generator and the transformer under test is necessary. This stiffer coupling reduces the effect of any failure in distorting the applied voltage wave.

Even more serious, the disturbance caused by the isolating gap, which is necessary when 60-cycle excitation is used, may mask a failure which would be detected, if 60-cycle excitation were omitted. It is probable that 60-cycle excitation masks more failures than it detects.

The hull-gap disturbances shown in Figure 11 of the paper can be eliminated by the use of a capacitance divider instead of a resistance divider. This has been our practice in order to eliminate as many of the masking effects due to 60-cycle excitation as possible. We question the use of the hull gap for the same reason as we question the use of 60-cycle excitation in that it may mask evidence of failure.

In our experience, we have never had a case where failure was indicated by 60-cycle power follow that was not indicated also by one or more of the other means of failure detection. Consequently, in the interest of simplifying tests and as a means of securing more accurate detection of failures, I agree with the author that serious consideration should be given to the omission of 60-cycle excitation during impulse tests and going further believe 60-cycle excitation should be omitted.

With reference to failure detection by means of oscillograms of the neutral current, this method shows promise and should be thoroughly investigated to determine its effectiveness in finding failures not shown by other means. Actually failure detection by comparing the voltage wave shape with the voltage wave shape at applied voltage is a very sensitive means of failure detection, particularly when 60-cycle excitation is omitted. Whether the additional complexity of adding an additional oscillograph is justified must be determined from experiment. Any new means of failure detection certainly is of interest in impulse testing to supplement the means already available.

Virgil F. Christen (Moloney Electric Company, St. Louis, Mo.): I want to say that our experience with the surge testing of transformers has been very similar to that reported by Mr. Hagenguth, although it is based on the results of a considerably smaller number of tests.

We find that the use of 60-cycle excitation with the impulse test considerably complicates the work of testing and also makes it more difficult to detect failure. In no case has the 60-cycle power-follow current helped us in detecting internal insulation failure, and in at least two instances, where it was necessary to remove the polarity gaps from some 15-kv high-current bushings before the test, the bushings flashed over; such heavy power current followed that the porcelain was damaged and had to be replaced.

The surge-generator hookup we generally use differs somewhat from that shown in Figure 1 of Mr. Hagenguth's paper by the fact that we make use of a capacitance voltage divider instead of the resistance divider in series with a vacuum spark gap and also by the fact that we do not use a capacitor similar to that shown as C_1 in Figure 1 of Mr. Hagenguth's paper to help in controlling the wave.

As far as we know, every failure during surge test, especially of power transformers, has shown up quite plainly on the full-wave oscillogram. This probably is attributable in part to the fact that we do not use capacitor C_1 and, therefore, have a considerably looser coupling between the surge generator and the transformer under test. But, for this same reason we frequently are unable to get as smooth a wave as we would like.

We also are bothered by the operation of the lightning arresters which we must use (in series with resistances) to prevent flashover of bushing of winding not being tested. The action of these lightning arresters frequently causes dips in the final full wave, which look exactly like insulation failures and which must be determined definitely to be due to the lightning arrester and not to anything else. Sometimes this requires repeating a considerable part of the surge test without the 60-cycle excitation.

When we have to use the 60-cycle excitation together with the impulse test to satisfy the requirements of specification we do so, but, whenever we are making a surge test for our own information only, we always omit the 60-cycle power.

I think it would be well for the manufacturers and purchasers of transformers to have the AIEE Standards committee look into this matter again with a view to eliminating the 60-cycle excitation during surge tests. I think the Standards committee should include some rules or instructions for averaging out oscillations on the back of the $1^{1}/_{2}$x40 wave so that, when a loose coupling between the surge-generator capacity and the transformer under test results in a wave which differs somewhat from the ideal, a satisfactory way to evaluate it could be established.

F. J. Vogel (Illinois Institute of Technology, Chicago, Ill.): Mr. Hagenguth's paper is very interesting inasmuch as it describes his experience and that of his associates in the impulse testing of transformers. It is to be expected that different observers with equipment of different design will have different experiences and arrive at different conclusions. I think that this is to some degree the case.

I believe it generally is agreed that impulse testing of transformers has been of great benefit to the industry. It has set definite levels to the strength of apparatus. As a result of the testing, weaknesses in the design and materials have been found, and it is probably impossible to estimate the value which this has been to the industry.

At the time that impulse testing was inaugurated, it was believed that impulse tests should simulate as nearly as possible the worst conditions which would be found in service. Because of this the original tests as proposed found fairly general acceptance pending the results of experience. I believe with Mr. Hagenguth that further consideration to these tests should be given now, but in my opinion it should be on the basis of changing them to make them simpler and easier to apply. At the present time they are complex and somewhat complicated, both from the standpoint of application and the test report.

Mr. Hagenguth has placed, in my opinion, undue emphasis on the difficulty of detection of impulse-voltage failure. This is not an inconsistent point of view since there was some opposition to the use of these tests from his associates at the very beginning. Theoretically, it is true that there is no proof that some failure has not occurred on impulse test. On the other hand, my own experience has indicated that practically no failures should occur that are not detected. Experience has shown that the cathode-ray oscillograph indicates some of the failures. The magnetic oscillograph has indicated several failures also. Smoke and oil disturbance has existed in nearly all cases of failure according to my recollection. The amount of smoke and oil disturbance is increased greatly by the 60-cycle excitation. Therefore I would conclude that the present methods are satisfactory from the detection standpoint. I grant, however, that the type of construction influences some of these factors, and the transformers with which I have been familiar have been well ventilated and the stresses well balanced. I know that in making these statements the objection may be raised that I am not progressive and that anything to make failures more easily found should be done.

In view of the fact that Mr. Hagenguth has made some proposals in his paper, it would be well to see what effects these might have with respect to the aforementioned problem. Present standards call for the smoothest waves possible when impulse tests are made. This was based on the belief that natural lightning waves would be smooth and that superimposed oscillations might alter the severity of the applied test. It is still my opinion that it is more desirable to make the test as nearly as possible a service test and therefore with as smooth a wave as possible. Failures at or near the line leads, in my experience, never have been hard to find. I know that this is not in agreement with Mr. Hagenguth's findings, but it seems to me that this is a very exceptional occurrence. Actually, superimposed oscillations conceivably could reduce the severity of the test and certainly would lead to variations of greater magnitude than in the past between different laboratories and would not make the test any simpler.

The omission of the 60-cycle excitation also would tend to a reduction in the severity of the test and an abandonment of the principle that the test should simulate service conditions. Suppose we consider a core-type 138-kv transformer with a value of alpha of 20. If this transformer had 60 coils, there might be at least 48 per cent of the impulse voltage between the first coil sections at the line. If the impulse test without 60-cycle excitation were applied and was about 750 kv, the stress between the coils would be about 360 kv. On the other hand, if the transformer were excited at 80,000 volts, the surge stress between coils would be practically 415 kv. Conditions would be even worse, if the transformer were excited at 138 kv when the coil stress would be about 450 kv. These are certainly appreciable increases due to the 60-cycle stress. It is of interest that one of the first core-type transformers which withstood impulse tests was of this type, and undoubtedly transformers of this type still are being made. Although considerable care is required in the design of such a transformer, it is not uneconomical, and transformers of this type probably are made by many manufacturers up to at least 69 kv. Besides this factor, however, some troubles in tap changers and taps at the grounded end have been found by failure due to the 60-cycle excitation. It is very likely that such failures also would be found by an oscillograph at the grounded end. So far as I can see, however, the result is only the substitution of one complication for another with the loss of some severity in the test. It certainly has not simplified greatly the application of the test.

To sum up, it seems to me that there is unnecessary alarm being raised regarding the detection of impulse failures, and that the alternatives proposed add further complication or are not likely to be sufficiently more sensitive to be worth while. There has been a demand for a simpler test procedure, and I think that such a procedure would be desirable. Proposals for these undoubtedly will be forthcoming in the future.

H. H. Wagner (Pennsylvania Transformer Company, Pittsburgh, Pa.): In this paper the author has made important contributions to the subject of impulse testing. Our own experience in commercial impulse tests on more than 200 power transformers, of a total of almost 1,000,000 kilovolt-amperes, confirms some of the author's conclusions which indicate the need for changes in the American Standards Association's *C57.2* test code.

I would like first to discuss the effect of normal frequency excitation. We have found in many cases that the wave shape is affected by the flashover of the necessary series gap used in the impulse-generator output circuit for protection of the generator, and by the flashover of the arresters or gaps used on the terminals not being tested.

Furthermore, we never have found any value in the 60-cycle excitation in detecting insulation failures, either by magnetic oscillograms, or by physical evidence of power follow such as noise and smoke.

In some cases the purchaser has specified that a final full-wave oscillogram be taken without 60-cycle excitation, in order to obtain a reliable comparison with the reduced-voltage full wave.

We, therefore, recommend that the test code be modified to omit the 60-cycle excitation on all transformer impulse tests.

With reference to failure detection we too have found by means of staged failure tests that failures may not be detected when a stiff coupling is used in an effort to obtain a smooth wave because of the low ratio of transformer current to impulse-generator current. I would add that especially is this true with regard to failures occurring between parts of the winding located farther than approximately 50 per cent from the line. Failures in the line half of the winding are usually easy to detect by comparison of wave shapes, even with stiff coupling and a smooth wave shape, where the failure involves parts of the winding as small as 1 per cent or less.

However, we usually have been able to use a fairly loose coupling between generator and transformer, giving a wave with oscillations but very sensitive to failures. We, therefore, suggest that the ASA test-code recommendation of a smooth wave be eliminated.

We have been investigating possibilities of failure detection other than those mentioned in the test code but have not completed our tests. It appears possible that the author's suggestion of recording the transformer current wave may prove to be a useful addition to the use of the voltage-wave oscillograms, and this method deserves further investigation. It would not be necessary to take the voltage and current oscillograms simultaneously, as the current oscillogram could be taken by applying one additional wave immediately after each regular full wave, thus requiring the use of only one cathode-ray oscillograph.

K. B. McEachron (General Electric Company, Pittsfield, Mass.): Through the years that 60-cycle excitation has been required in connection with impulse testing of transformers, considerable investigation has been made with reference to the matter of power follow. The kilovolt-ampere rating of the power source, including step-up transformers, together with the constants of the power source circuit already have been given consideration, but it is difficult to provide a practical method which will insure power follow, either across a bushing which flashes over or between coils and turns, when a failure occurs in a transformer.

It is believed that this is not unlike service, for there seems to be some evidence that lightning damage has occurred in unprotected transformers and other apparatus without power follow. It is well known that insulator flashovers occur on transmission lines as a result of lightning without power follow. In fact in connection with the tests made in 1929 and 1930 at Croton Dam, Mich., on the line of the Consumers Power Company, some study was made of the gap spacings required to be sure that power follow would take place when gaps in parallel with insulator strings were flashed over by impulses produced from a 1,500,000-volt impulse generator at one end of the transmission line. These tests demonstrated the difficulty involved in setting up conditions which would insure power follow in each case.

The impulse tests cannot be regarded as being satisfactory unless reliable means of detection are used, and apparently the 60-cycle excitation method is not sufficiently reliable to be depended upon for this purpose.

P. L. Bellaschi (Westinghouse Electric and Manufacturing Company, Sharon, Pa.): According to the paper a good proportion of the lightning failures of transformers in the field which have occurred in the past should not have occurred, for according to Table I power current seldom would follow impulse failure in windings and parts. And this again raises the point that the power supply and the impedance of the power circuit are important requirements in impulse testing. Regardless of these and other criticisms I wish to commend the author for the contribution he has given us.

In May 1938 a report entitled "Use of Power Excitation With Impulse Testing" was presented to the AIEE transformer subcommittee. In it we reviewed typical cases where defects in coils and sections would have escaped detection had power excitation been omitted. The experience of the Westinghouse Company in impulse testing transformers—which now totals some 5,500,000 kva—shows that the full wave gives an over-all check of the entire winding and, in fact, of other windings of the transformer not under direct impulse test. It further corroborates that in combination with power excitation the full wave provides an effective test and method for detecting shortcomings in materials and workmanship, and other deficiencies that may exist across coils and sections. In particular, such test will reach and check the adequacy of the insulation deep into parts of the winding which otherwise can escape attention. Under certain conditions even turn-to-turn failures can be detected as the result of power-follow current.

In the event of a failure our procedure is as follows: From the cathode-ray oscillograms, the magnetic oscillograms, and other observations during the test the character and location of the failure can be diagnosed closely in many cases before any additional tests are made. This presupposes a thorough knowledge of impulse-testing technique, of transformer and insulation characteristics, and, in fact, of the unit and design under test, though infrequent extraneous effects due to the impulse generator or to the associated measuring equipment may occur. These should be identified and cleared; there should be no hesitancy to repeat the entire test and to go beyond the conventional number of impulse applications in cases such as these to clear up all reasonable doubts. Repeated full waves with power excitation have been an effective method to determine the cause of the irregularities and to localize them when they are in the transformer. By repeated waves I have reference to 10, or 20, and in some cases more, applications. When advisable, other impulses have been applied such as full waves without power, chopped, and steep-front waves. With one exception—in no case where failure was indicated by test have we failed to find the failure after disassembly of the transformer. In many a life test, in which transformers were repeatedly subjected to front-of-wave and full-wave life tests well up to their levels and in which the transformer performed satisfactorily, disassembly and minute inspection confirmed the integrity of the insulation. By all this we do not want to imply that the present methods, even in the best hands, are infallible. To this we may add that indeterminate cases may develop in the course of time, and, in such an event, dismantling may be in order.

In the order of importance we consider the full wave a basic test; then the front-of-wave, as this simulates conditions resulting from direct strokes; and, third, the chopped wave.

A word on the progress and the benefits that have accrued to the industry from impulse testing. Here are a few. Since 1930 there has been a 50 per cent and better over-all improvement in the impulse strength of transformers. Design has not been stereotyped, it has remained dynamic. There has been a continual control of materials and workmanship. An impulse generator in the midst of an assembly aisle certainly has a salutary effect. The benefits also have reached out to the entire station through better and more certain methods of co-ordination and protection.

Lately, attention has been called to routine testing. Both in the transformer subcommittee and in a present discussion representatives of two of the larger operators have expressed their favorable view on routine impulse testing. We are much in sympathy with them. In fact, as early as 1939 the Westinghouse Company initiated a routine test on distribution transformers.

There have been a number of suggestions on the ASA test code, *C*57.2, since the present transformer standards were issued in 1942. For my part I believe that the test code is fundamentally sound but susceptible to improvements and to further simplification and clarification. This is a matter for study and consideration by the AIEE transformer subcommittee.

Milan Getting, Jr. (Allis-Chalmers Manufacturing Company, Pittsburgh, Pa.): We have at our disposal at the present time, five different tests for investigating the insulation of transformers. Power factor and Megger tests reveal the condition of the insulation, chiefly with respect to the moisture content. The remaining three tests: applied potential, induced potential, and impulse tests actually stress the insulation. The fact that we find ourselves today with three tests indicates that each one must stress the insulation in a different way. After approximately ten years of impulse testing, the question is being raised whether or not it is advisable to continue combining two of these tests when impulse testing a transformer (excitation of the transformer at power frequency and voltage may be considered as a form of applied potential test).

The question, it seems to me, may be resolved easily by first of all considering our objective. We can ask ourselves two questions: Are we trying to determine whether or not power-follow current occurs after impulse breakdown? Or, are we trying to demonstrate the ability of a transformer to withstand impulses? Tests can be designed to answer either question.

When the art of impulse testing was first developed, there were a lot of questions that needed to be answered all at once. Among these, one of the most pertinent was: Will power current follow impulse breakdowns in a transformer? It was only natural once artificial-lightning generators were available that this test should be performed at the most convenient time and place where such equipment was available, that is, during the final tests of the transformer on the factory test floor. Today we have heard many people say, and it is also our own experience, that power follow current occurs only in rare cases. Therefore, it is of only very minor assistance in the detection of impulse breakdowns. On all the other occasions when power-follow current does not occur, it is merely a time- and labor-consuming nuisance of no value to anyone. The only value of combining excitation with impulse test lies in operational or design data, and this question has been answered.

If, on the other hand, our objective is to demonstrate the ability of a transformer to withstand impulses, then any step which will facilitate or make a more sensitive method of detecting impulse breakdowns available would seem to be one in the right direction. Following this line of reasoning, we find ourselves in accord and heartily endorsing the proposal made by J. H. Hagenguth that excitation be eliminated, and instead oscillograms of current flowing in the grounded end of the winding be taken. The method will have to be investigated to see whether it is applicable to all sizes of transformers. One difficulty that occurs to us at the moment is whether sufficient deflection can be obtained with cathode-ray oscillographs now in use for the small currents that flow in the windings of the distribution transformers. A year's study should develop the necessary information to establish a satisfactory technique.

H. C. Stewart and J. E. Holcomb (General Electric Company, Pittsfield, Mass.): During the last seveal years, the distribution transformer engineering division of the General Electric Company has studied the effect of impulse testing on a great many transformers. Disassembly and examination after test have shown that layer failures occurred which had not altered the oscillographic trace or were not indicated by the presence of smoke or bubbles.

As a result of our experience, we believe that the present method of failure detection may not show turn or layer failures.

This seems reasonable, since in some instances it is difficult for smoke or bubbles to get out of buried layers with sufficient velocity to be readily noticeable to the observer. It is also our experience that with excitation on the test piece, normal-frequency current seldom follows. Therefore, it is evident that the present procedures for fault detection are not entirely satisfactory.

Smaller transformers may require different failure-detection methods than those discussed in this paper for large transformers. The following methods have shown promise for use in the impulse testing of distribution transformers;

1. The use of a loosely coupled impulse generator much as described in the paper but with a slow sweeping speed, since the phenomena out on the tail of the wave can indicate more accurately failure in layer-wound coils.
2. Another method involves the observation of internal oscillations as an indication of the change in impedance occasioned by a fault within the winding. To utilize this method, the transformer must have a tap at which point an oscillogram can be taken.
3. A modification of the foregoing method involves the observation of the oscillations on a winding other than the one under test. Such a procedure provides even looser coupling and can be expected to provide profound changes in wave shape, when winding impedances are altered by internal faults within any of the transformer windings.

In conclusion, it is of the utmost importance to establish the electrical condition of apparatus after the application of an impulse test, and considerable work remains to be done to provide the tools for this purpose.

J. H. Hagenguth: The large number of discussions indicate that impulse testing of transformers and certain details of the test are of importance from various points of view.

With the exception of Mr. Bellaschi and Professor Vogel, all discussers are agreed that the omission of 60-cycle excitation during the impulse test should be considered seriously by the AIEE transformer subcommittee. It is particularly gratifying that Mr. Pierce and Mr. Whitehead, as representatives of the operating group who had an exceptionally great experience with impulse testing, are in accord with such a move. In this connection it is of interest that another large purchaser of transformers has made normal-frequency excitation an optional requirement to be applied at the discretion of the manufacturer. All this points definitely to the conclusion that the normal-frequency excitation has not fulfilled the expectations published[1] 12 years ago. I quote,

> "The problem of detecting insulation failure is solved, however, by the simultaneous application of surge voltage and normal-frequency exciting voltage, so that failure is accompanied by the flow of power current" . . . "Experience has shown that with the surge and power voltages synchronized in this manner, power current will follow in every case in which the transformer insulation fails to withstand the surge voltage."

Mr. Bellaschi's explanation of his company's procedure in the event of failure is enlightening with respect to this approach when he mentions:

> "Repeated full waves with power excitation has been an effective method to determine the cause of the irregularities and to localize them when they are in the transformer."

Although Mr. Bellaschi does not express clearly how the failure is identified in the first place—I presume by cathode-ray oscillogram—it seems that he finds it necessary to apply numerous (10 to 20 and more) impulse waves before definite proof of failure is obtained by 60-cycle follow current. He does not outline the procedure to be followed in case failure is indicated on the cathode-ray oscillogram, but follow current is not established after repeated application of the test.

The 1938 report, "Use of Power Excitation With Impulse Testing" by Mr. Bellaschi does not give at all conclusive evidence that 60-cycle excitation was needed to detect the failures.

Of the four cases cited, the report mentions in only one case: "This is a case where the presence of power excitation enabled detecting the failure." However, a comparison of cathode-ray oscillograms submitted shows small but definite differences in wave shape. In two other cases, it is stated: "Cathode-ray oscillograms show insufficient and in fact practically no evidence of the internal failure." In the fourth case, where cathode-ray oscillograms gave very great differences in wave shape, the report states: "Power excitation and the magnetic oscillograph record are usful in confirming the failure."

This report and Mr. Bellaschi's discussion, therefore, show that little reliance was placed on the cathode-ray oscillogram indications of failure. I get the definite impression that 60-cycle excitation is used primarily to burn out a fault, once it has been detected by other means. As pointed out in the paper the exact location of the failure in the winding can be found without the use of excitation.

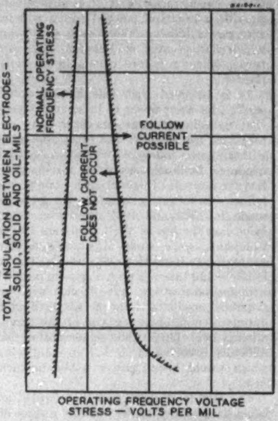

Figure 1. Relation between operating-frequency stresses and the possibility of power-follow current after impulse puncture

In the discussions, I was impressed with the lack of evidence that power-follow current had detected a failure which was not also detected by cathode-ray oscillogram.

Our laboratory experience and a review of our commercial impulse testing show that 60-cycle follow current is to be expected across small portions of the winding after puncture by impulse, only when unusually high 60-cycle stresses exist.

We have never found follow current after puncture due to impulse where the normal 60-cycle safety factor existed. Two curves showing the relation between 60-cycle excitation required to produce follow current and the normal operating stresses used are shown on Figure 1 of this closure.

Professor Vogel is of the opinion: "That undue emphasis has been placed on the difficulty of detection of impulse voltage failures." I wonder, if I misinterpret Professor Vogel's statements which indicate that he considers that a transformer has failed only when there is a 60-cycle power-

Table I. Per Cent Voltage Across Portions of Winding

	Due to Full Wave									Due to Chopped Wave			
	0.5 μsec Wave Front			1.5 μsec Wave Front			2.5 μsec Wave Front						
Per Cent Winding	No Excitation	With Excitation	Per Cent Increase Due to Excitation	No Excitation	With Excitation	Per Cent Increase Due to Excitation	No Excitation	With Excitation	Per Cent Increase Due to Excitation	No Excitation	With Excitation	Per Cent Increase Due to Excitation	Per Cent Increase Over 1.5 μsec Front With Excitation
100–98.2	29	34.3	18	18.3	21.2	16	13	14.5	11	57.8	55.3	−4	173
98.2–96.2	39.6	44.8	13	29.7	32.4	9	19.7	21.7	10	63.4	63.3	0	96
96.2–92.6	47.4	54	14	38.1	43.7	15	29	31.9	10	65.3	63.3	−3	49
92.6–89.3	40.8	47.4	16	34.7	40.8	18	29	31.9	10	56.4	56.3	0	38
89.3–85.2	39.6	44.8	13	31	35.3	14	28.2	31.9	13	52.2	50.8	−3.5	48
85.2–82	36.4	40.8	12	29.7	33.8	14	26.1	29	11	46.5	45	−3	37
82–78.3	32.9	36.9	12	29.7	33.8	14	25.2	27.6	9	40.8	40.8	0	21

follow current. Our experience has been that 60-cycle follow current occurs in a very small percentage of the failures.

In many cases the difficulty exists in convincing oneself that a small irregularity in wave shape not accompanied by power-follow current definitely means a weak spot in the insulation structure. In our case, when only the smallest wave disturbance without power-follow current indicated such weak spots, disassembly of the transformer and meticulous examination of the insulation always has revealed the failure.

Furthermore, Professor Vogel in his remarks apparently fails to appreciate that the purpose in perfecting impulse testing technique was to make it possible to adopt impulse testing as a standard test. In view of his attitude, I would like to reiterate the basic purpose of impulse testing. The purpose of the impulse test is to insure the manufacture of transformers insulated throughout to withstand the stresses imposed by certain specified impulse waves. This means that every part of the winding withstands these stresses. It is not the intent merely to weed out only those transformers where power failure occurs on the first few lightning applications.

Professor Vogel also brings up the increase in stresses produced by 60-cycle excitation as a deterrent to its omission. This factor has been recognized and mentioned in the paper. Apparently some amplification is needed to show that the presence of 60-cycle excitation does not contribute to increases in maximum coil-to-coil stresses in a winding as described by Professor Vogel.

The maximum stresses are produced by the chopped wave and are independent of normal-frequency excitation. Table I shows the results of tests of coil-to-coil voltages on a winding having an initial voltage distribution with an alpha of approximately 20. The 60-cycle excitation crest was 23 per cent of the impulse-wave crest. The table clearly shows that the maximum coil-to-coil stresses are independent of normal-frequency excitation, even for the type of winding discussed by Professor Vogel. As a matter of fact, the variations in stresses due to permissible variations in wave front from 0.5 μsec to 2.5 μsec, are of the order of 30 per cent to 40 per cent as compared with 9 per cent to 18 per cent increase due to excitation. Whereas the stresses resulting after the wave is chopped, which are independent of the excitation, are from 20 per cent to 170 per cent greater than those produced by the rising front before the wave is chopped, when excitation is applied.

The fact that transformers have failed due to lightning in the field was brought up by Mr. Bellaschi as an argument for using 60-cycle excitation during factory tests, implying of course, that only 60-cycle follow current resulted in discovery of the failure in the field.

It is common knowledge that transformers are punctured in the field by lightning without power-follow current which would require taking the transformer out of service. It is frequently the repetitive action of lightning strokes combined with continuous excitation over relatively long periods of time which will result in final failure then, of course, detected by power-follow current. In order to assure that failures are detected during the test where only a limited number of impulses are applied, the power-follow current is, to say the least, a very undependable failure detector.

This is in accord with Doctor McEachron's observations of the power follow on transmission lines. From this we can deduce that follow current is not a necessary adjunct to impulse failures even in the field.

The power supply and its impedance are, of course, of importance. We find that the kilovolt-ampere rating of the excitation equipment used by the General Electric Company exceeds the ratings used by the Westinghouse Company.

I believe Mr. Getting has summarized very nicely the normal-frequency problem, and I hope that in the interest of economy, as well as better failure-detection methods, normal-frequency excitation will be omitted as one of the requirements of the impulse test. As a matter of fact this requirement is one of the major obstacles to impulse testing of all power transformers. We agree with Mr. Pierce that a move in that direction is desirable, if and when the test can be sufficiently simplified to be economical. The fact that it is permissible, and often necessary, to omit 60-cycle excitation for the final full wave clearly shows that the excitation is undesirable in many cases.

The circuit constants, the relative physical location of leads, test gaps, voltage dividers, and so forth, now are being considered by an AIEE committee preparing a standard for impulse testing. Although a capacitance divider would help a little in the matter of gap disturbances, the circuit still is subject to disturbances from the gap between the transformer and impulse generator. Furthermore, the ratio of the resistance divider can be determined by simple means, and is, therefore, preferable from the point of view of accuracy. The load capacitance, when used, performs the same function and is of the same order of magnitude as the inherent effective stray capacitance to ground of other types of generators, and, therefore, its effect on the failure detection will be of the same order.

The question of the importance of different types of test depends entirely on the method of protection employed in the field. In those cases where gaps are used for protection, the steep-front test, chopped-wave test, and full-wave test are of equal importance to demonstrate the withstand volt–time level of the transformer for the different types of surges to which it may be subjected in the field. In the case of lightning-arrester protection, supplemented by effective ground-wire installation a few thousand feet out from the station, the transformer will be subjected to full waves only, and the necessity of a steep-front test and even a chopped-wave test is debatable from the point of view of adequacy for service conditions.

As should be expected, the introduction of a new method for failure detection meets some skepticism. However, this method does not complicate the test, requiring either one more oscillograph or two additional impulse applications for the reduced full wave and the full wave. As stated in the paper, this method should be given a trial in order to find, if it has limitations. For distribution transformers a neutral resistance shunt of a few hundred ohms will give sufficient voltage to register on the cathode-ray oscillograph. The methods of detection discussed by Mr. Stewart and Mr. Holcomb are of interest and may be preferable for distribution-transformer work.

REFERENCE

1. IMPULSE TESTING OF LARGE TRANSFORMERS, F. D. Newbury. ELECTRICAL ENGINEERING, volume 51, April 1932, pages 252–4.

Hot-Spot Temperatures in Dry-Type Transformer Windings (44-103)

Design and Operating Characteristics of Modern Dry-Type Air-Cooled Transformers (44-171)

Discussion and authors' closures of paper 44-103 by H. C. Stewart and L. C. Whitman, and paper 44-171 by W. W. Satterlee, presented at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944, and published in AIEE TRANSACTIONS, 1944, October section, pages 763–8 and 701–04.

V. M. Montsinger (General Electric Company, Pittsfield, Mass.): The paper, "The Design and Operating Characteristics of Modern Dry-Type Air-Cooled Transformers," is interesting and timely, and should be of value to the industry in that it gives some service tests and operating experience on the use of a high-temperature insulation in dry-type transformers having temperatures above 130 degrees centigrade.

Mr. Satterlee concludes that these insulations can withstand continuously as much as 160 degrees centigrade for long periods of time. Generally speaking, there are two ways of determining the maximum temperature that a new insulation can withstand. When possible, both of these methods should be used. One method most commonly used is by laboratory aging tests; the other method is by operating experience. Each method has its advantages and limitations. Aging tests at different temperatures and based on physical conditions can be made in a reasonable length of time, but they sometimes are suspected of indicating too short a life for a given temperature limit. Operating experience, on the other hand, requires a long time, is generally inconclusive, and is likely to be too optimistic because of

1. Lack of a continuous maximum temperature.
2. Lack of knowledge of the condition of the insulation after a given period of operation.

The fact that a transformer is still in operation does not prove that the insulation has not been injured seriously. Even though a transformer stands the high-potential tests as cited in the paper under "Service Tests," this does not mean necessarily that its internal insulation has not been weakened seriously. This is especially true if the tests are made following a load-

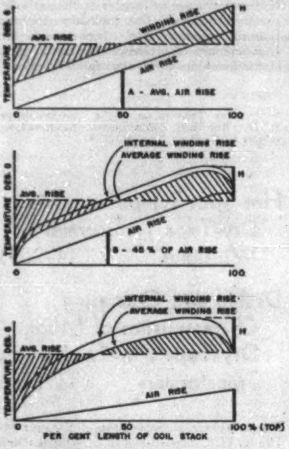

Figure 1. Temperature gradients of cooling air and winding for dry-type transformers

H—Hot-spot increment
Top. Ideal—Hot-spot increment = average air rise
Center. Natural draft—Hot-spot increment = 45 per cent of air rise
Bottom. Fan cooled—Hot-spot increment > total air rise

ing condition. Whether or not a dry-type transformer stands the high-potential tests depends mostly on the amount of moisture in the winding, and there should be no free moisture following a 16-hour load period when one of the tests cited was made.

The author states: ". . . tests made on windings treated with this bond show that it will withstand continuous temperatures of the order of 160 degrees centigrade for long periods of time with but little loss of its mechanical cementing properties." In terms of life expectancy of a transformer of 20 to 30 years, the aforementioned "long periods of time" must have been rather brief.

One very important function of varnish in dry-type transformers is to protect the insulation from moisture. Excessive temperatures for long periods of time may not reduce seriously the mechanical cementing properties of the varnish, but they will cause checking and loss of film properties of the varnish. This will reduce the dielectric strength as well as permit absorption of moisture which will jeopardize the transformer still further. We should not lose sight of the fact that it is far more important to retain the varnish film on dry-type coils than on oil-immersed coils, for the reason that air which fills the cracks has only a fraction of the dielectric strength of oil that flows in and fills up the cracks.

Moisture absorption seldom will be serious while the transformer is carrying load or even while carrying only excitation. Heat from core plus draft of cooling air induced by core losses generally will keep windings reasonably dry. However, the operating cycle of many transformers includes occasional periods with transformers completely disconnected from the line. After the insulating varnish of such a transformer has been deteriorated by long periods of operation at excessive temperatures, moisture absorption during idling periods may result in trouble when the transformer is re-energized.

Mr. Satterlee states that, where temperatures above 130 degrees centigrade may occur, the voltage is limited to winding turn-to-turn stresses of not over 80 volts. This intimates that the danger of breakdown between turns or between coils is rather remote, because of the low normal voltage stress. The stresses between turns and coils can be many times normal as a result of impulses, switching, arcing grounds, and so forth. Experience with other types of transformers shows that the danger of breakdown within the winding is generally greater than breakdown from line to line or from line to ground. This means that good dielectric strength must be maintained from turn-to-turn and from coil-to-coil throughout the life of the transformer.

As to the best procedure to follow in setting up new temperature limits for new insulations, it no doubt will be necessary to employ both service experience and laboratory aging tests. In making laboratory tests the life of the class-*B* insulations can be judged fairly well by power factor, insulation resistance, brittleness or crazing, and dielectric strength. While dielectric strength is a poor criterion for judging the amount of aging of insulation immersed in oil, it is a very good criterion for judging the condition of insulations in dry-type transformers. If the varnish cracks or disappears, the dielectric strength decreases to a low value. It is suggested that the various laboratories conduct aging tests along these lines. The results, supplemented by service experience, can then be used as a basis for considering a new temperature limit or limits for these high-temperature insulations.

I want to congratulate the authors of the paper, "Hot-Spot Temperatures in Dry-Type Transformer Windings," on the very thorough job they have done in carrying on this investigation. These new data will be very useful to the industry for many years to come.

I wish to discuss the paper from the standpoint of the relationship between hot-spot increment (or difference between the hot-spot and average winding rise) and the air rise. Ordinarily if the vertical temp rature gradients of the winding and air are straight lines the hot-spot increment should be equal to one-half the air rise.

The tests show that in all cases there is some bend-over in the winding vertical temperature gradient due to conduction of heat out through the ends; consequently, the hot-spot increment should be somewhat less than half the air rise. For the natural-draft runs on the inner coils this is true, the hot-spot increment being in the order of 40 to 45 per cent of the air rise. This is logical and needs no further explanation.

However, when we examine the fan-cooled runs we find that the hot-spot increment is generally equal to and in some cases considerably greater than the total air rise. For example: Figure 5 shows that the hot-spot increment for the inner coil is 30 degrees centigrade as against an air rise of 28 degrees centigrade (given in the seventh line, Table I of the paper) and the hot-spot increment for the outer coil is 29 degrees centigrade as against an air rise of only 22 degrees centigrade. Offhand, this does not appear to be logical. The reason for this is not given in the paper, and the principal purpose of my discussion is to explain how it is possible to obtain a hot-spot increment equal to or greater than the air rise.

This apparently illogical thermal characteristic of the fan-cooled tests is due to several factors, the more important ones being:

1. The higher copper-loss densities result in higher internal-coil gradients, and for the same average rise by resistance the bend-over in the upper vertical-coil gradient is greater than in the natural-draft heat runs. Compare Figures 4 and 5. This reduces the hot-spot more than it reduces the average rise. It is not enough however to account for the total effect.

2. The vertical gradient from the bottom end of the coil up to the point where the turnover starts bends over more in most cases than it does for natural-draft cooling. This again reduces the hot spot more than it reduces the average rise.

3. The air rise is, of course, much less for the fan-cooled than for the natural-draft runs, and this coupled with the increased internal temperature gradients produces a greater difference between the hot-spot increment and the air rise.

4. The fact that the foregoing three factors added together produce a greater difference in the ratio of the hot-spot rise and average rise for fan-cooled than for natural draft (which means that there is a greater difference between the top and bottom temperature than for natural cooling with the same average rise) creates a further difference in the copper losses at the bottom and top of the coils. This effect is, of course, not very large, but it is a contributing factor.

The foregoing factors are illustrated in the three sketches, designated as "ideal," "natural draft," and "fan cooled." Two significant points will be noted in comparing the "ideal" with the "natural-draft" figure:

1. There is a difference between the internal-winding rise and average winding rise throughout the length of the "natural-draft" coil stack.

2. The temperature gradient in the "natural-draft" stack bends over near the top.

The same two points may be noted in the figure for "fan cooled" except that the conditions are accentuated.

C. P. Xenis (Consolidated Edison Company of New York, Inc., New York, N. Y.): Mr. Satterlee's paper clearly summarizes his extensive investigation, supported by tests, of temperatures reached at various points of dry-type air-cooled transformers as well as hottest spot temperatures. He shows that with proper design each part of the assembly will not exceed the temperatures it can withstand safely. The insulation at points where the temperature of 130 degrees centigrade is exceeded is at windings where turn-to-turn stresses do not exceed 80 volts. The service tests and favorable operating experience quoted by the author justify the increasing popularity of the dry-type air-cooled transformer.

The first dry-type air-cooled transformer with class-*B* insulation manufactured by the Westinghouse Electric and Manufacturing Company was completed in December 1936 and installed on the distribution system of the Consolidated Edison Company of New York, Inc., in March 1938. It has been in operation ever since. This unit is a three-phase 13-kv 500-kva transformer installed in a transformer vault at grade level with a large louvered door facing the street. The only maintenance required was the removal of dust from the windings, busses, insulators, and other parts of the unit in July 1940, utilizing a vacuum cleaner for the purpose. No other cleaning operation or repair of any kind has been necessary to date.

Three single-phase 13-kv 167-kva dry-type air-cooled transformers were put in operation in September 1941, and 12 100-kva 13-kv dry-type air-cooled transformers in December 1942. We also purchased and installed on our system on November 18, 1943, a dry-type air-cooled transformer completely enclosed in a metal tank for operation as a submersible unit in a street manhole. The operation of all these transformers has been trouble-free, and no maintenance of any type has been necessary to date.

We have a strong preference for tank-enclosed dry-type air-cooled transformers, because they permit the operation of this type of equipment in street manholes where water is frequently present, and also because they exclude moisture, dust, and so forth, from the windings on indoor installations.

The dry-type air-cooled transformer with type-*B* insulation as compared to conventional oil-filled equipment possesses features which are of considerable interest to utilities operating electric distribution systems. In case of electrical failure, the amount of organic insulation which can be consumed by the arc at the fault is relatively small; hence the probability of generating large quantities of explosive gases is minimized. Further, in the case of open-type transformers, the gases and heat generated by the fault are released immediately to the atmosphere, whereas in liquid-filled equipment such faults frequently generate pressures which rupture the tank and cause other damage.

Dry-type air-cooled transformers manufactured to date generate more noise than corresponding liquid-filled transformers. This is a serious objection when the equipment is to be installed in residential areas, and it is hoped that Mr. Satterlee and other transformer design engineers also will obtain a happy solution to this problem.

P. L. Bellaschi (Westinghouse Electric and Manufacturing Company, Sharon, Pa.): Dry-type power transformers have been a subject of considerable discussion in the AIEE transformer subcommittee. From the two papers presented at the AIEE summer technical meeting the thermal-distribution characteristics for such apparatus become well established, both papers arriving at substantially the same conclusion. The paper by Mr. Satterlee proceeds further into the practical problems of design, the application of insulating materials, and field experience. Because of the increasingly wide field experience, the paper should be of particular interest to the user. This is only natural when some 1,000,000 kva of dry-type power transformers of the design, construction, and insulating materials described in the paper have been in service, some as long as seven years. It is also well to point out that at no other period have transformers in industry been so fully loaded and overloaded as in the past three or four years, and this type of transformer certainly has had this service, too. We therefore are dealing with apparatus that is becoming more and more seasoned by years of intense and successful field experience.

In view of all this, it is to be expected that the question of standardization should come to the fore. This is a matter that has been debated in the transformer subcommittee for several months. The paper therefore should provide the basis for progress in such standardization work and is of particular interest to the transformer committee.

There are already established precedents in standards for temperatures appreciably in excess of 130 degrees centigrade. Class-*B*-insulation standards recognize such practices. For instance, American Standard *C*-35.1, 1943, (AIEE Standard 11), which covers rotating electrical machinery on locomotives and rail cars, recognizes temperature rises by resistances of 120 degrees centigrade for the armature part and 130 degrees centigrade for the field. In this Standard, the limits of observable temperatures recommended for service (normal values in Table I) are 145 degrees centigrade for the armature and 155 degrees centigrade for the field. To these must be added the hot-spot allowance for this class of apparatus—at least ten degrees centigrade, and the sum gives the hottest-spot temperatures.

W. C. Sealey (Allis-Chalmers Manufacturing Company, Milwaukee, Wis.): The authors of the paper, "Hot-Spot Temperatures in Dry-Type Transformer Windings," have presented considerable factual data on temperatures in dry-type transformers. The variety of dry-type transformers is great and extends from small sizes to large sizes. It is obvious that with a wide variation in size and type of construction there likewise will be a wide variation in electrical characteristics, including the correction for hot-spot-temperature rise.

An analysis of the reasons for the hot-spot correction is informative and provides a proper base for any rules which are proposed for the hot-spot correction of dry-type transformers.

In dry-type transformers the hot-spot correction is due to two principal causes:

1. The rise in air temperature as the air flows through the ducts, causing the top of the coil to be hotter than the bottom of the coil.
2. The radial gradient between strands of the conductor which causes a difference in temperature between adjacent strands of conductor.

The effect of temperature rise of the air in increasing the hot-spot correction can be reduced by insuring equal distribution of air in the ducts and by supplying a large quantity of air, so that its temperature rise through the duct will be small.

The hot-spot correction because of the radial gradient between adjacent strands can be reduced by reducing the number of strands through which the heat must pass in order to be transmitted to the air stream. If every individual piece of copper is exposed to the air duct the effect of the radial gradient on hot-spot correction is negligible.

A minor agent in hot-spot correction is the end effect which causes the end of the coil stack to be cooled better than the body of the coil stack as a result of conduction of the heat at the end. In most cases this is not a major factor in the hot-spot correction.

Because of these fundamental factors, the hot-spot correction is a matter of design and, consequently, as shown by the data of the authors, no universal correction or universal factor which is generally applicable is possible. However, any standard which is set up should make provision to encourage the use of designs with low hot-spot correction in cases where such a design is economical. For class-*B* insulation a statement that the hot-spot-temperature rise should not be more than 20 degrees above the average limiting temperature would be a means of taking care of this factor and would permit the retention of the present 80 degrees centigrade rise by resistance for class-*B* insulation in the present American Standards.

F. W. Bush (Allis-Chalmers Manufacturing Company, Milwaukee, Wis.): As shown by the papers by Stewart and Whitman and by Satterlee, there is a wide variation in hot-spot corrections to be applied to the average temperature rise by resistance to obtain the hot-spot copper temperature in dry-type transformers. This wide variation exists whether the correction is stated as a fixed amount in degrees centigrade or as a ratio. When expressed in degrees centigrade, it varies from 13.1 to 42.6 degrees. When expressed as a ratio it varies from 1.19 to 1.56. This wide variation is to be expected because of the wide variation in the physical sizes and designs of dry-type transformers.

These two papers show very clearly the need for more precise definition of limiting hot-spot temperatures than is contained in the present American Standards. In order to conform to past practice a retention of 55 degrees centigrade rise by resistance for class-*A* insulation, and 80 degrees centigrade rise by resistance for class-*B* insulation is desirable. The limiting hot-spot temperature for the transformers could be ten degrees higher than the limiting rise by resistance for class-*A* insulation, and 20 degrees higher than the limiting rise by resistance for class-*B* insulation. This would permit the engineer who wishes to design for low hot-spot corrections to do so, and at the same time would still protect the user by limiting the hot-spot temperature rise. It would also have the advantage of keeping the same temperature rise by resistance standards as existed in the past.

W. W. Satterlee: Mr. Montsinger states: "The fact that a transformer is still in operation does not prove that the insulation has not been seriously injured." What one desires is that the transformers continue in operation. The facts are, as cited by Mr. Xenis, and as stated by the author, that many of these dry-type transformers have been in continuous operation for more than seven years. Nearly 2,000 transformers have operated continuously for periods of one to over seven years without a single winding failure occurring as the result of hottest-spot temperatures, an accumulation of dirt, or absorption of moisture by the insulation. The author cites several tests made on actual transformers lasting over two years which were conducted under extreme conditions of temperature and moisture. No winding failures occurred, and little or no deterioration of the insulation resulted. It should be recognized that seven years operation may represent approximately one third of the expected average life of any transformer.

The author recognizes that under transient conditions turn-to-turn stresses may exceed normal voltage stresses. However, no operating difficulties have been experienced. Surge tests made on actual transformers have demonstrated the sufficiency of the insulation. While these transformers are designed normally for indoor operation, they may be satisfactorily protected against lightning surges.

H. C. Stewart and **L. C. Whitman:** The discussion by Mr. Montsinger clearly illustrates the reasons for the deviation of the measured hot-spot increments from the theoretical value of one-half the air rise in the ducts that was previously proposed. It is especially important to recognize the fact that fan cooling will not by itself lower hot spots if the full rise by resistance is maintained.

Mr. Sealey suggests that hot-spot increments be reduced by reducing the number of strands through which the heat must pass in order to be transmitted to the air stream. It is difficult to foresee much improvement on inner windings by the use of additional ducts. For example, in Figure 12 of our paper we see that the maximum radial gradient (hottest to coolest strand) is only about 1.5 degrees centigrade for a four-strand winding operating at 72.9 degrees centigrade average rise and cooled by natural draft. Also, practically all this drop occurs only one strand in from the outside. If *all* the radial gradients were eliminated in such a winding cooled by natural draft, this would only lower the hot spot in a 75-degree-centigrade-rise 30$^1/_8$-inch-long winding, from 30.5 to 29 degrees centigrade. In the case of a fan-cooled winding, we see that the maximum radial gradient for a 67.8 degrees centigrade average rise is eight degrees centigrade (see Figure 10 of the paper), with six degrees centigrade of this rise occurring one strand from the outside. Hence, a duct every two strands would reduce the radial gradient only two degrees centigrade. For a 70-degree-centigrade 30$^1/_8$-inch-long winding operated with a 530-feet-per-minute air velocity, this would reduce the hot-spot increment from 29.5 to 27.5 degrees centigrade.

The suggestion of a larger amount of air through the ducts to reduce the air rise and thereby reduce the hot-spot increment works only to a limited degree, as shown by comparing hot-spot increments for air velocity of 490 and 1,515 feet per minute. The quantity of air through the ducts is approximately three times as much in the latter case, but the hot-spot increment is reduced only about ten per cent (see Table III).

Increasing the duct width on natural-draft units also accomplishes only a minor reduction in hot-spot increment. The authors' tests indicated that, while changing from a one-half-inch duct to a one-inch duct reduced the air rise to about 65 per cent of its original value, the hot-spot increment was changed less than ten per cent.

Mr. Satterlee's paper, "The Design and Operating Characteristics of Modern Dry-Type Air-Cooled Transformers," also calls attention to tests made on a model winding, in which only about a 25 per cent decrease in hot-spot increment was noted in going from one-half-inch to one-inch air duct (see his Figure 5, and note that 1.0 must be subtracted from his ordinates to give hot-spot-increment ratios).

From this information, we must conclude that only minor reductions in hot-spot increments can be accomplished by Mr. Sealey's suggestions. Possibly, structures can be designed to reduce further these hot-spot increments, but it has not been demonstrated by published tests that any such structures have been designed or built. This being the case, it would seem reasonable to recognize the high hot spots that exist in normal air-cooled transformer windings and co-ordinate the allowable rise by resistance with the demonstrated hot-spot increments.

Mr. Bush has emphasized the wide variations in size, type of construction, and method of cooling, that are encountered in dry-type air-cooled transformers. The subject paper deals with transformers cooled by vertical and horizontal air ducts. The hot-spot increment has been found to vary considerably with the vertical height of the coils and so in general with the transformer kilovolt-ampere size. As noted in the paper, there was no measurable effect whether the coils were in open air or enclosed in a metal shell, provided there was adequate ventilation at top and bottom. The paper does not cover the very small dry-type units that are mainly cooled by heat conduction to the case rather than air ducts, and rules covering such designs conceivably would be different.

Since hot-spot temperatures are not measurable on production units, it would seem that retention of the present rise by resistance limits as suggested by Mr. Bush will allow hot-spot temperatures in excess of present standards.

It is the opinion of the authors that the present standards of hot-spot temperatures should be adhered to until such time as information is available to prove that class-*B* insulations are suitable for hot-spot temperatures in excess of 130 degrees centigrade.

High-Voltage Compressed-Gas Power Transformers

Discussion and author's closure of paper 44-170 by Henry M. Hobart, presented at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944, and published in AIEE TRANSACTIONS, 1944, September section, pages 690–2.

Jerome J. Taylor (The Detroit Edison Company, Detroit, Mich.): Mr. Hobart's ideas regarding compressed-gas insulation for high-voltage cables and transformers have wide scope, are stated aggressively and interestingly, and raise dozens of stimulating questions.

The present paper incorporates two major proposals, which are very different in application and effects. One relates to the merits of compressed-gas insulation, while the other advocates use of power frequencies in the relatively high range of 600–1,200 cycles per second. The author seems to infer that the two must be considered concurrently, mainly on the assumption that large increases in core losses, per unit volume, would result unavoidably in damagingly high oil temperatures. This assumption seems open to question, since very high heat-transfer coefficients can be obtained between iron and oil. The actual oil temperature thus becomes a function of rate of circulation and design of heat exchanger, and can be reduced to almost any desired figure. The advantages claimed for 600–1,200-cycle operation of high-pressure transformers are therefore applicable to conventional oil-insulated units, and the two proposals can be considered separately. This is helpful because it simplifies analyses of them.

In regard to insulation, the author has focused attention on dielectric strength of compressed gases "in the clear," rather than on the more practical design problem of providing safe creepage distances over solid insulators. This point seems important as applied to his cable designs, which contemplate the alignment of three conductors inside a small steel pipe, by means of solid spacers. As applied to transformers, the author speaks as though nearly all solid and fibrous insulation would become unnecessary in compressed-gas units. It is not clear what items could be eliminated by going from oil to gas, because solid materials are needed for support, bracing and conducting-particle barriers in conjunction with any fluid insulation.

The high-frequency proposal is very interesting, the more so because the electronics people are rapidly unfreezing many of the traditional frequency restraints, and because expanding uses show as clearly as ever that no single "style" of electricity has a monopoly of desirable qualities. Offhand, however, 600–1,200-cycle frequencies appear better adapted to transformers and (as the author mentions) welders, than to switchgear and long transmission lines. If this is true, large amounts of frequency conversion equipment would be required, and an inclusive cost comparison becomes complex, even with all technical factors known.

Mr. Hobart's system is perhaps in somewhat the same situation as frequency modulation and high-voltage d-c power transmission have been. If this analogy is strong enough, his system should be tried on an initially moderate scale and operating experience accumulated.

Arthur A. Bolsterli (Moloney Electric Company, St. Louis, Mo.): Mr. Hobart deserves a great deal of credit for his bold approach to the subject of compressed-gas power transformers. At a time when compressed air is successfully competing with oil in the field of circuit breakers, Mr. Hobart's proposals are in line with the present trend and deserve careful scrutiny and analysis.

Technologically at least, the stage would seem to be set for an advance in the direction in which Mr. Hobart points. During the last year or so a dry-type transformer of the subway type, which is hermetically

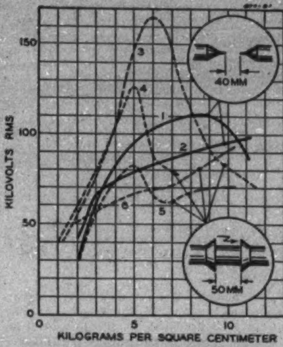

Figure 1. Breakdown voltage (rms) between needle points with a spacing of 40 millimeters and along a leakage path between sharp edges spaced 50 millimeters apart, in function of the gas pressure

Alternating voltage, 50 cycles
1, 4—Pure gas path in air
5—Nitrogen
2, 6—Carbon dioxide
3, 5, 6—Breakdown along porcelain tube
4—Breakdown along bakelized paper tube

sealed in its tank, has made its appearance. It, too, uses internal forced blast. Obviously, this is the forerunner of Mr. Hobart's transformer from which it differs in that the internal pressure is not appreciably higher than atmospheric.

As to specific points in Mr. Hobart's paper, the working temperatures which he envisages for core and windings seem, at first sight, rather high. We know that in the so called dry-type transformer with class *B* insulation we cannot let the copper reach temperatures at which the insulating varnish begins to deteriorate, because we depend on this varnish for moisture protection and turn insulation. In compressed gas, this limitation, however, does not hold. The gas takes the place of the solid impregnant. The glass covering on the wire serves as the spacer or matrix and can now be worked to its true temperature limit. Considerably higher maximum temperature than heretofore thought permissible is therefore technically feasible. Economically, the valuation of capitalized losses will, however, set definite limits to the densities permissible in core and copper, and these may or may not permit the full utilization of such high temperature limits as are advocated in the paper.

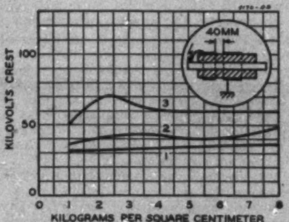

Figure 2. Flashover voltage (peak) in compressed air along a steatite insulator with concentric metal conductor connected to one electrode, in function of the pressure

1—Alternating voltage, 50 cycles
2—Fifty per cent flashover impulse voltage, wave $^1/_{50}$ microsecond, conductor connected to positive pole
3—As for 2 but with conductor connected to negative pole

The curves show the ineffectiveness of increasing the pressure in order to raise the flashover voltage, in the case of surface discharges both for alternating and impulse voltages

In connection with the turn insulation mentioned, the following facts should be kept in mind. In a reasonably uniform field the breakdown strength increases fairly close to proportional to gas pressure. However, when solid matter and gas are in such intimate association as, for example, in paper or glass fabrics or the glass covering of wire, the increase in electric strength is much slower. For dry paper, for example, the breakdown strength at ten times atmospheric pressure (air) is somewhat less than three times that at atmospheric pressure. Similar relations hold for glass fabrics and the glass covering of wire. It is obvious that the designer of the compressed-gas transformer must adapt his design strictly to the peculiarities of the new medium, for which the rules are different from and perhaps more complicated than those he has become accustomed to for oil.

Take, for example, the subject of creepage. Figure 1, which is taken from the September/October, 1943, issue of *Brown-Boveri Review*, shows a comparison of breakdown strength versus air pressure for a point gap and a creepage path issuing from sharp-edge electrodes over porcelain and bakelite. Note that within the range of pressures shown, the breakdown strength for creepage shows a peak and then decreases in spite of increasing pressure. Figures 2 and 3 (from the same source) further illustrate the fact that uniform benefit is not always obtained from an increase in pressure. Figure 2 refers to a typical bushing arrangement. Figure 3 illustrates how a material improvement in flashover versus pressure performance was obtained by a clever modification of the cap in the case of a conventional supporting insulator. These examples are shown merely to point out that the different elements making up the design must receive detailed attention and that the choice of optimum gas pressure is a matter of research from more than one angle.

To obtain the best conditions for cooling, the heat from losses should be transferred to the tank with the least possible temperature drop. For forced blast inside the tank, as proposed by Mr. Hobart, this transfer should offer no difficulties. It is interesting to note that the "watts per square inch per degree centigrade" increase as the eight-tenth power of gas pressure, for forced convection, which is quite an improvement over the one-half power which holds for natural convection. For dissipation from tank surface to the ambient air, the same methods and rules apply as for the conventional transformer.

Mr. Hobart proposes the use of sulphur dioxide. It would be interesting to know more about its comparative properties for insulating and cooling. As far as heat transfer under forced convection is concerned, the following comparison may be of interest. If one assumes the gas passing through ducts at a given velocity in feet per second, regardless of the nature of the gas, it would seem from accepted relations (which however have never been experimentally proved for high pressures) that there is little difference, in the heat transfer coefficient, between air, helium, and sulphur dioxide. For hydrogen it is about 40 per cent to 50 per cent higher. There would be, of course, considerable difference in the amount of power required to force these gases through the ducts at equal velocities, the advantage being on the side of the lighter gases such as helium and hydrogen. From this point of view sulphur dioxide, being considerably heavier than air, would not seem particularly advantageous.

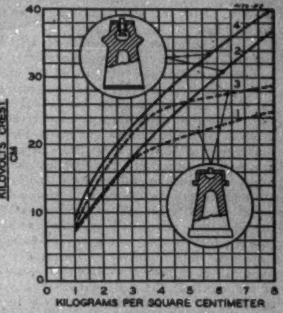

Figure 3. Flashover gradients (peak kilovolts per centimeter) of porcelain supporting insulators in compressed air-in function of the pressure

Older design of smooth supporting insulator on right. New design with penetrating electrode on left
1, 2—Flashover gradient for alternating voltage (50 cycles)
3, 4—Gradient for 50 per cent flashover impulse voltage, positive wave, $^1/_{50}$ microsecond

There still is an almost unlimited scope for research on the dielectric and thermal behavior of gases under pressure. Tests of this nature, when made on a scale comparable with large-scale application, are extremely laborious and costly and require equipment which has seldom been available in the past. Today many of our universities and industrial laboratoris are well equipped to explore this field and we may well look forward to a real advance in the knowledge of these phenomena which have fascinated physicists and electrical engineers for some 50 years.

H. Weichsel (Wagner Electric Corporation, St. Louis, Mo.): It is always a very courageous undertaking to advocate methods diametrically opposed to those which have been standard for many years. Nevertheless, because certain designs are accepted practice, it does not necessarily follow that no further improvements in this particular line of designs are possible or even advisable. It is a queer historical fact that many engineering developments start out with certain relations, later developments accept relations just the opposite to those used first, and finally the designs return to the direction used in the original attempts. It only has to be remembered that the a-c system first made use of frequencies materially higher than used at present, then the tendency swung to frequencies as low as 16 cycles before going to the present 60 cycles. If history repeats itself, Mr. Hobart's suggestion of frequencies much higher than the present 60 cycles will be used in the future. Undoubtedly, before this day can arrive, a lot of work, theoretical as well as experimental, must be done.

Mr. Hobart not only suggests a considerable increase in frequency but also recommends the use of gas under high pressure as a cooling and insulating means. On first thought, Mr. Hobart's suggestions appear to reach into an entirely new field but upon second thought it is realized that this field has already been invaded in one or more forms. For instance, large rotary machines operating in a hydrogen atmosphere are an example of gas-cooled units. I clearly remember the days when this step was advocated and the average engineer looked at it with great doubt and suspicion, but machines operated in hydrogen have proved their value because of the excellent thermal characteristics of this gas. Static capacitors operating in a chamber filled with nitrogen under heavy pressure are now, and have been for many years, in successful operation in the United States as well as abroad and form a good example of apparatus operating in high-gas pressure. These examples show that practical experience has been gained in connection with some of the problems arising in connection with Mr. Hobart's suggestion. There are a large number of other problems connected with his suggestion to which, at present, no definite answers are available. For instance, the very high operating temperatures advocated present many problems in connection with insulating materials and their applications. Many new mechanical problems will arise due to the large expansion of parts subjected to the high temperatures.

Another field which must be studied carefully is the behavior of gases under high pressure when subjected to surges. Little work, if any, has been done in this direction. If my recollection serves me correctly, several years ago some investigator did some work along these lines which, however, did not give very encouraging results.

Other engineering problems enter into the final solution of the main problem. One of these problems is the feasibility of high-frequency motors. This field during the last few years also has been partly invaded in connection with motor applications for airplanes. In addition to these many unsolved or partly unsolved engineering problems, we are confronted by the everlasting, dominating factor of economy but the solution to this problem cannot be approached until more about the engineering possibilities is known.

At this stage of the art, we only can thank Mr. Hobart for this audacious approach to this matter which, it is hoped, will stimulate further investigation along the lines the author suggests. Only by this kind of an attitude is real progress possible. It is hoped that one day Mr. Hobart's work will form the cornerstone for a new era.

Herman Halperin (Commonwealth Edison Company, Chicago, Ill.): In the second paragraph of the paper and elsewhere, the author talks about transmitting large amounts of power over overhead-transmission lines at frequencies as high as 600 to 1,200 cycles per second. Even if the transmission-line voltages are much higher than now used, it is difficult to see how the author's proposal would make it feasible to transmit very much power through the greatly increased reactances which would obtain with frequencies of 600 to 1,200 cycles. Nowadays we have plenty of difficulty taking care of 15, 20, or 30 per cent voltage drop due to the reactance of 60-cycle lines and multiplying these reactances by 10 or 20 seems appalling.

Another problem would be the matter of increased insulation and greater spacings required for higher voltages than the present general limit of 230 or 287 kv.

H. H. Skilling (Stanford University, Stanford University, Calif.): The question of increased reactance of transmission lines naturally arises in connection with Mr. Hobart's suggestion of higher power frequency. In general, reactance being proportional to frequency, low frequencies have been thought of as desirable for transmission.

However, an increased frequency of transmission has been mentioned[1,2,3] as a possible means of providing good stability and voltage regulation for a power line. It is one of several methods that can be used to balance capacitive reactance against inductive reactance, thereby providing good transmission conditions. If, as Mr. Hobart predicts, a frequency substantially greater than 60 cycles becomes desirable because of its advantages in transformers and other terminal equipment, it will be found that there can be definite advantages in transmission, also. The frequency to be used should be selected with these factors in mind.

In 1936[3] it was suggested that the insulating properties of compressed air and other gases required study with a view to their use in electric-power transmission. This research has been undertaken by a number of investigators, with results that are finding considerable application in engineering practice. Mr. Hobart gives information regarding dielectric properties of certain gases, and the bibliographies of references 4, 5, 6, and particularly 7 refer to most of the literature on the subject.

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3. Useful Range of A-C Power Transmission, H. H. Skilling. *Electrical World*, volume 106, 1936, pages 453–5, 514–15.

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7. Breakdown Studies in Compressed Gases, A. H. Howell. AIEE Transactions, volume 58, 1939, May section, pages 193–204.

W. C. Sealey (The Allis-Chalmers Manufacturing Company, Milwaukee, Wis.): The author has expressed his thoughts well on the development of a compressed-gas power transformer. Transformers such as he describes can be built. An experimental transformer of this type was described by Mr. Stafford of our Pittsburgh works in a paper presented before the Compressed Air Institute in 1942. This experimental transformer is also described in the November 14, 1942, issue of the *Electrical World*. The sample transformer was rated 50 kva, 2,400 volts, 60 cycles, and was designed for operation on 150 pounds per square inch gas pressure. This transformer was tested and successfully passed the tests applying to a standard dry-type transformer including heat run.

The principal advantage of this unit was the elimination of oil while providing a sealed-tight construction which would not be affected by moisture, dirt, and so forth.

The principal disadvantages of such a unit are the necessity of maintaining the high pressure and the increased cost of construction, especially the cost of the tank to provide for the high pressure.

Wayne J. Morrill (Electric Motors and Specialties Company, Fort Wayne, Ind.): Mr. Hobart's very radical suggestion of changing our standard distribution frequency from 60 cycles to 600 is at the least stimulating. Undoubtedly, there are many reasons why such a move should not be made but if, in order to allow free play of our imagination, those reasons be ignored, the possibilities in connection with small motors are very interesting.

As long as we are contemplating such an important change in our distribution system, we might as well go the whole way and carry polyphase 600-cycle power directly to the residences, thus in one swoop simplifying and reducing the cost of all the many small motors that are used in the home.

With the use of polyphase 600-cycle power would come the following advantages in connection with small motors:

1. Motors weighing about one half to one fifth as much as the present motors of equal speed and horsepower.

2. High-speed motors of about one tenth the weight of present slow-speed motors of equal horsepower.

3. Motors free from switching devices such as are needed for starting high-torque single-phase motors.

4. The possibility of commutatorless motors operating at speeds as high as 34,500 rpm for two-

pole motors and 18,800 rpm for four-pole motors. This might bring greater quietness to vacuum cleaners and it would certainly bring increased service-free life to such machines.

5. The single-phase hum of the present household motors would be eliminated by the use of polyphase power, although the higher line frequency might still make itself apparent in the noise from the motor. In any event, it is probable that 600-cycle polyphase motors could be made very satisfactory from a noise point of view.

Henry M. Hobart: The contributions which have been made to the discussion of my paper, entitled "High-Voltage Compressed-Gas Power Transformers," are so predominantly characterized by valuable, constructive proposals, that the author is disposed to express to the discussers his appreciation of their good opinions and also to extend his thanks for the many excellent suggestions and counsels, and only briefly to touch on a few points which ought to be straightened out.

In the paper, the author explained that his task has been chiefly an attempt to devise a system of power transmission embodying compressed-gas transformers and in utilizing in this system various ideas of brilliant pioneers of this and past eras. At the time of the conception of some of these ideas, various necessary links in our scientific and engineering knowledge still were lacking, so that the author's contribution has been largely of a co-ordinating nature, in which he has endeavoured to so utilize the old ideas and the new knowledge as best to provide the desired result.

The contributions to this discussion include a considerable amount of new and pertinent material and also warnings and counsel regarding possible difficulties still remaining to be surmounted. So far as it still lies within the author's power, he proposes thankfully to accept and profit by these further data and good counsel, but he could wish that many others, in laboratories and elsewhere, would be disposed to work actively upon the still outstanding tasks required for successfully completing and demonstrating the system.

The author's text has been misunderstood by Mr. Halperin (and possibly by others) namely, in concluding that it is proposed by the author to employ higher frequencies, for instance, 600 cycles, with *overhead* high-voltage power transmission lines. The author is quite aware that at 600 cycles, (1) the telephone interference factor would be hopelessly great and (2) that the corona loss at the desired high voltages would be so great as to be utterly prohibitive. A careful reading of the very first paragraph of the paper will show that the author's project relates exclusively to a system in which *overhead* conductors spaced many feet apart are replaced by conductors spaced only several *inches* apart, and enclosed in a metal pipe-line filled with compressed gas. Conductors so located will not occasion any interference in communications systems, whatever the periodicity.

Mr. Taylor states that in oil transformers a sufficiently high speed of circulation of the oil would permit keeping down the temperature of the oil below its sludging point. However, it is the author's opinion that the power required for such high-speed circulation would too seriously impair the transformer's efficiency. Furthermore, as stated in the discussion of Table II, the breakdown strength of the oil, both for sinusoidal stresses and impulse stresses, is much below that of suitable gases sufficiently compressed.

The author especially welcomes Mr. Taylor's statement to the effect that "the electronics people are rapidly unfreezing many of the traditional frequency restraints." Not only in welding applications, but also in many other electrical-engineering applications, the great savings in cost made possible by using transformers of much higher frequencies (such as 600 cycles), will effect important reductions in the total capital outlay for the applications. As one example, there is the case of main-line railway electrification in which, today, there is an almost complete lack of interest because of a belief that nonelectric systems have proved their superiority. The author is in agreement with some electric-railway specialists, that one of the most valuable electrification systems is to supply, let us say, 3,000 volts, from rectifier substations. The transformers required in such rectifier substations represent a very large item in the total substation cost, but they just as well could be for 600 cycles instead of for 60 cycles (and their cost then would be only one seventh as much) if only the transmission lines provided 600 cycles. Due to gradual improvements, the cost and weight of modern d-c railway motors for the locomotives would be out of all proportion less than in the old days when main-line railway electrification was an active subject. The aggregate of the savings which could be made under these modern conditions leads to a much better economic showing for railway electrification versus the rival nonelectric propulsion systems than could have been made prior to their availability.

Similar great reductions in cost could be effected by the substitution of 600-cycle transformers in place of 60-cycle transformers in such cases as the enormous rectifier installations employed in the production of aluminum and magnesium.

This is not an occasion justifying enlarging further on this subject of the many advantages of higher frequency in various other electrical-engineering applications, except briefly to state that not high-periodicity transformers alone, but also many kinds of high-frequency generators and motors as well as reactors and capacitors, may be built with much less weight, lower cost, and better characteristics than are attainable with 60-cycle designs.

The other contributions to the discussion, not as yet specifically mentioned in this closure, nevertheless are appreciated and valued by the author. In his opinion, they are so clearly expressed as to very effectively carry their own helpful messages, which will be very useful to others and to the author when continuing the work on this project. One of these contributors is Mr. Bolsterli, and the material which he makes available, together with his interesting comments and advice, will be decidedly helpful. It was in 1932 (12 years ago), that the author was privileged to study an excellent and convincing article by Mr. Bolsterli, in which was pointed out the superiority over oil of compressed gases for insulation and heat-transfer in transformers and capacitors. Had it not been for his study of that article, the author is of the opinion that the present paper never would have been written. It is encouraging to the author to find that Mr. Bolsterli is still on the job.

In conclusion, the author is most eager to re-emphasize the fact that in his work on these very extensive subjects, his own all-too-meager knowledge and experience have been checked and greatly supplemented by the collaboration of many specialists in matters coming within their respective fields of experience but he has realized that it is incumbent on himself alone to bear the responsibility and to press forward with this task to the extent that circumstances may permit. However, the abilities and experience of these "silent partners," afford, in his opinion, ample guarantees that the system is sound and of great importance to the electrical industry. With the added interest evinced by these helpful contributions, the author will strive to obtain practical experimental results with the least possible delay and, in this further work, he believes that he will continue to have a reasonable amount of counsel from his many and very generous specialist collaborators.

It would seem that there now has been accumulated plenty of material to justify curtailing discussion and actually doing some of these things along the lines described in his paper, or else in still better ways which others may devise. The cost of the production, transmission, and application of electric power thereby may be greatly decreased with benefit to the electric light and power industry and to commercial and residential consumers.

Economics of Trolley-Coach Operation

Discussion and author's closure of paper 44-96 by G. M. Woods, presented at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944, and published in AIEE TRANSACTIONS, 1944, August section, pages 578–81.

F. VonVoigtlander (Faraday Electric Corporation, Adrian, Mich.): The author's paper is of great interest to all concerned with urban public transportation. The popular acceptance of the trolley coach is a matter of record throughout the country, brought about largely by the swift, silent, odorless, and economical performance of these vehicles. Operators are trained easily if they have had previous experience in handling steerable vehicles, and equipment availability of from 95 to 98 per cent is quite ordinary.

The author makes the statement that it can be assumed that, for equal standards of convenience and excellence, shops, car houses, garages, and similar items will be about the same, regardless of the type of vehicle selected. The writer's experience would indicate that here again the trolley coach has a considerable advantage over the gas bus. Maintenance facilities required will be found to be approximately proportional to one minus the availability factor. Service facilities can be cut to an even greater proportion, as there is no refueling to be done, and there are no crankcases or radiators to fill on the trolley coach, though greasing, washing, and cleaning would be about the same. In the wintertime there is no concern about difficult engine starting or freezing of radiators, so that the trolley coaches can just as well be parked outside in the yards, thereby eliminating storage facilities. In fact, the only significant disadvantage of the trolley coach is its necessary confinement to electric overhead facilities.

In a situation where the electric trolley coach is being considered to replace gasoline or Diesel-electric busses the cost of providing electrical facilities is of great importance. Substations for the conversion of a-c power to d-c power for trolley-coach operation become an important part of the cost of these facilities. In the outer fringes of the system, the loads may be light, but voltages must be made adequate for the starting of at least one vehicle at the end of the line. Providing for such minimum voltages is often a problem. The development of small fully automatic rectifier stations has helped, but, considering the fact that the modern trolley coach is powered with a series-type motor, it would appear to the writer that the direct application of alternating current to the propulsion of the coach should be given really serious attention. The trolley-coach system then could be fed from the distribution network, with transformers located where required. Since 600 volts direct current has met with universal favor in the light-traction field, it would seem that the desired range for a-c operation would be between 750 and 2,300 volts single phase. Recent developments in switchgear and contactors should make the direct use of 2,300 volts entirely feasible.

The use of alternating current for propulsion also would permit the use of tapped autotransformers for motor control which would be of some advantage during the period of the year when heat is not required for the vehicles. Power costs could be reduced somewhat thereby and over-all efficiency increased. Construction of trolley coaches for 2,300-volt operation might require some increases in insulation of the vehicle's electric apparatus, as leakage to ground must be held to very low limits to protect against even the remote possibility of an unsafe potential developing between vehicle and ground. This requirement has been solved very effectively for the 600-volt d-c vehicles, and it does not seem that 2,300-volt a-c operation should present any serious problems here. The whole matter of power conversion could be eliminated, and proper voltages could be provided much more readily through the flexibility of alternating current by the use of regulators or tapped transformers connected to the distribution network.

G. M. Woods: The idea of operating transit vehicles by alternating current has intrigued a number of engineers. The operation of a-c trolley coaches particularly has received considerable study. In addition to the hazard of high-voltage leakage, as mentioned by Mr. VonVoigtlander, other considerations have precluded the development of a-c trolley-coach equipment so far.

Most central-station power supply is at 60 cycles. At the present state of the art a 60-cycle traction motor with the necessary associated apparatus would be impractical because of weight and cost. Successful 25-cycle traction motors have been built in quantity, but such motors of suitable size and characteristics for trolley coaches will weigh more and be more expensive than a suitable d-c motor. To obtain 25-cycle power from the usual 60-cycle power supply requires frequency-changing apparatus comparable to the conventional a-c–d-c substation.

The a-c trolley coach would take power from only a single phase, thus unbalancing the generator load or requiring phase balancing or single-phase generation. To realize any distribution economy, the alternating trolley voltage would have to exceed the practicable motor voltage, and this would dictate the use of a transformer on the coach. The provision of suitable space for an adequate transformer would be difficult.

Since the use of alternating current for propulsion of trolley coaches would add weight, cost, and maintenance expense to the vehicle itself; increase the possibility of leakage; and in most cases require frequency conversion; there seems at this time to be little promise for this application. The use of ignitrons simplifies the a-c–d-c conversion system, improves the efficiency, and reduces both installation cost and operating expenses.

A Resonant-Cavity Method for Measuring Dielectric Properties at Ultrahigh Frequencies

Discussion and authors' closure of paper 44-161 by C. N. Works, T. W. Dakin, and F. W. Boggs, presented at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944, and published in AIEE TRANSACTIONS, 1944, pages 1092–8.

C. V. Larrick (General Electric Company, Schenectady, N. Y.): In the measurement of low-loss dielectric materials in the range covered by the authors, one of the problems is the reduction of metallic losses in the conductors of the measuring circuit. In those methods where a section of coaxial line is filled with the dielectric, the sample, in addition to being long, must be of rather large cross section to reduce the ratio of metallic to dielectric loss. As the frequency is increased into the long microwave range, the dielectric losses increase more rapidly than the metallic losses. However, at the same time, relatively small diameter lines are used, so that the problem of metallic losses remains. It would appear that the arrangement of the authors will give less metallic loss than a resonant line arrangement of the same diameter. The purpose of this discussion is to calculate the magnitude of the improvement. Thus, consider the arrangement of the authors in its simplest form: namely, a short-circuited coaxial-line section of electrical length βl terminated in a capacitor, which contains the dielectric (Figure 1). The input conductance of the line if the end plate is neglected is

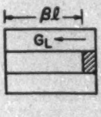

Figure 1

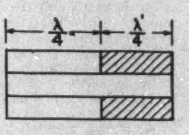

Figure 2

$$G_L = y_0 \frac{\alpha l}{\sin^2 \beta l}\left(1+\frac{\sin 2\beta l}{2\beta l}\right)$$

where

y_0 = surge admittance of the line
αl = total attenuation in the line

The susceptance of the line, which is also that of the capacitor, is

$$B = y_0 \cot \beta l$$

The conductance of the capacitor is

$$G = y_0 \cot \beta l \tan \delta$$

where $\tan \delta$ = dissipation factor = G/B. The ratio of the two conductances is

$$\frac{G}{G_L} = \frac{\tan \delta}{\alpha l} \frac{{}^1/_2 \sin 2\beta l}{\left(1+\frac{\sin 2\beta l}{2\beta l}\right)}$$

At $\beta l = 45$ degrees this expression has a maximum of value:

$$\left(\frac{G}{G_L}\right)_{\max} = \frac{2.45 \tan \delta}{\alpha\lambda}$$

In the case of coaxial lines, the combination which occurs is a quarter wave length of empty line, followed by a dielectric sample a quarter wave length long (Figure 2). The surge admittance of the empty line is y_0, and the attenuation constant is α as before. Then if the end plate is neglected, the conductance of the empty line as seen from the junction with the dielectric is $y_0\alpha(\lambda/4)$. The metallic conductance of the line containing the sample as seen from the same point is $y_0\alpha\sqrt{\epsilon'}(\lambda/4)$. Hence, the total metallic conductance is

$$G_M = y_0\alpha\frac{\lambda}{4}\,(1+\sqrt{\epsilon'})$$

The attenuation constant produced by the dielectric is

$$\alpha_d = \frac{3.15}{\lambda}\sqrt{\epsilon'} \tan \delta$$

from which the dielectric conductance is

$$G_d = 0.788 y_0 \tan \delta$$

The ratio of the two couductances is

$$\frac{G_d}{G_M} = \frac{3.15\sqrt{\epsilon'} \tan \delta}{\alpha\lambda(1+\sqrt{\epsilon'})}$$

If we compare the ratios of dielectric to metallic effects for the two methods

$$\frac{(G/G_L)_{\max}}{(G_d/G_M)} = 0.78\left(1+\frac{1}{\sqrt{\epsilon'}}\right)$$
$$= 1.27 \text{ for } \epsilon' = 2.5$$

Thus, for most materials, the arrangement of the authors gives a slight reduction in metallic losses at all frequencies. Also, the losses in the end plate touching the sample will be less in the method of the authors than in the resonant line since the currents flowing will be smaller.

Robert F. Field (General Radio Company, Cambridge, Mass.): The authors of this paper have shown great skill in combining the simplicity of operation of the susceptance-variation method of measuring the electrical properties of dielectrics with the low losses of the resonant-cavity method. Their use of micrometer-controlled electrodes and flat disk samples entails, however, the necessity of determining accurately the fringing at the outer edge of the electrodes and of accepting Hartshorn's double-measurement method of eliminating the radial resistance of foil electrodes.

The assumption that fringing is independent of the dielectric constant of the sample, while not strictly correct, is usually valid within the experimental error of the measurements, providing the sample is no larger than the electrodes. Sets of measurements with the susceptance-variation circuit, in which the thickness of samples of the same material is varied over a wide range, have yielded values of dielectric constant identical within one per cent, just as do the data given in this paper. Scott[1] also proved this to be so in developing his formulas for the fringing capacitance.

The values of dissipation factor obtained by the authors for quartz and polystyrene show that Hartshorn's method of correcting the radial resistance by measuring the sample both with and without foil electrodes is still valid at 200 megacycles. Some doubt has been expressed on this point, because this series resistance varies as the $^1/_2$ power of the frequency, and at times the correction at 100 megacycles has been larger than the true value. Since, however, this type of error is minimized when all surfaces are plane and parallel, the consistency of the reported results may be taken as indicating very good machining of both electrodes and samples.

Reference

1. Edge Correction in the Determination of Dielectric Constant, A. H. Scott, H. L. Curtis. *Journal of Research*, National Bureau of Standards, volume 22, June 1939, pages 747–75.

William A. Yager (nonmember; Bell Telephone Laboratories, Inc., Murray Hill, N.J.): The method described by the authors for measuring the dielectric properties of insulating materials in the 100- to 1,000-megacycles range is sound and practical but not new in principle. The same principle of a lumped capacitance tuned by a distributed inductance in the form of a coaxial transmission line was employed by D. L. Hollaway[1] in the construction of his Dielectrometer, an instrument for measuring conductances and dielectric properties at 100 megacycles. The Dielectrometer is a single re-entrant cavity in which the loss tangent is determined either by frequency or susceptance variation, the detuning in the latter case being accomplished by means of a vernier micrometer capacitor projecting into the head of the re-entrant post. The author's equipment, on the other hand, is a double re-entrant cavity in which the loss tangent is determined by a combination of frequency and susceptance variation, detuning being accomplished by varying the electrode separation of the re-entrant posts, which is a critical adjustment and requires the use of a differential-screw drive. The Dielectrometer relies on sliding contacts which the authors have eliminated by means of a sylphon bellows. The circuit Q of both instruments appears to be of the same order of magnitude.

A true evaluation of the relative merits and ease of operation of the two instruments could be made only by one who has made measurements with both. The writer prefers detuning by means of a vernier capacitor, and the use of a differential-screw drive is somewhat questionable to him. However, the symmetry of design, the elimination of sliding contacts, and the reduction in the electrical-field distortion in the vicinity of the dielectric sample are advantageous features of the author's design which become more and more important as the frequency increases.

In a recent paper, C. R. Englund described a method and equipment for dielectric constant and power-factor measurements at centimeter wave lengths. The coaxial test specimen is positioned at the quarter wave point of a half-wave-length coaxial tuned cavity terminated at one end by a fixed short and at the other end by a sliding coin-silver plunger provided with contact fingers to insure intimate contact with the inner and outer conductors. The loss tangent of the dielectric test specimen is determined by detuning the resonant line length with and without the test specimen. The dielectric constant is obtained by the line shortening necessary to restore resonance on the introduction of the test specimen.

Extensive dielectric measurements have been made on low-loss materials in the writer's laboratory at 1,300 megacycles by this method with an accuracy of ±0.00005 for power factor and one per cent for dielectric constant. The test specimens are coaxial cylinders 0.640 inch outside diameter by 0.173 inch inside diameter, varying in length from one-quarter to one inch, depending upon the dielectric constant of the material. Test specimens of this size can be readily molded or machined, but the preparation of ceramic or glass specimens is of course more difficult. It is believed that this method should be practical down to frequencies of the order of 500–600 megacycles. At lower frequencies the test specimen becomes somewhat cumbersome, and the type circuit described by the authors becomes more practical. A careful consideration of all the factors involved probably would show that the practicability of one method increases as that of the other decreases in the frequency range from 100 to 1,000 megacycles.

Englund's method has the following advantages which become more important considerations as the frequency becomes higher:

1. The circuit has greater symmetry.
2. The field distribution in the vicinity of the test specimen is more uniform.
3. As a consequence of (2) the edge correction is negligible.
4. The detuning process is less critical.

On the other hand, the computations by the author's method are less time consuming, the geometry of the test specimen is simpler, and sliding contacts are avoided. However, sliding contacts on the Englund instrument have not been found to be particularly troublesome if reasonable precautions are exercised in regard to cleanliness and handling.

In conclusion, it is felt that the type of equipment described by the authors has a definite place and is a worthy contribution in the field of dielectric measurements. The authors are to be complimented for the careful consideration given to the many theoretical and design details so essential for accurate and reliable measurements.

Reference

1. D. L. Hollawy, *Transactions* of the Institution of Engineers (Australia), XXI, 79 (1940).

Karl Zimmermann (Federal Telephone and Radio Corporation, Newark, N. J.): The ultrahigh-frequency measurement of the dielectric constant and the power factor of solid dielectrics, particularly low-loss materials, is considerably simplified by the resonant-cavity method described in this paper. Although the coaxial transmission line is available for liquid dielectrics, the difficulties encountered in sample preparation makes it impractical, in many cases, for solid dielectrics. There is also a decided advantage in a test method that does not require perfectly faced and smooth samples. Samples from production runs can be rapidly tested and the results returned in time to obtain control.

The apparatus, although precision made, is compact and sturdy. The required lengths for 50 to 1,000 megacycles are convenient. Considerable difficulty is eliminated by the absence of leads and electrodes that have to be attached to the sample.

The authors are to be congratulated upon their application of the resonant cavity to the technique of ultrahigh-frequency measurement of dielectric properties.

T. W. Dakin and **C. N. Works**: Mr. Field's experimental results in the measurement of dielectric constant using the susceptance variation circuit substantiates the authors' contention that within the limits of accuracy of the measurements fringing is independent of the dielectric constant of the sample. The paper by Scott and Curtis of the National Bureau of Standards, as Mr. Field points out, supports this claim. The American Society for Testing Materials has accepted the susceptance-variation method for measuring the dielectric constant and power factor of materials.

The use of foil electrodes on the sample is standard practice with the susceptance-variation circuit at frequencies below 100 megacycles. With this resonant-cavity method at frequencies of about 500 megacycles, the need for the use of foil is decreased if the surfaces of the sample are reasonably parallel. Foil is needed only where the most accurate possible results are desired. Although the authors have assumed that Hartshorn's double-measurement method for elimination of the resistance of the foil electrodes is valid, the correction, especially at the higher frequencies, is often within the limits of accuracy of the method and a second measurement is not necessary except when the ultimate possible accuracy is desired.

As Mr. Field points out, our electrodes were plane and parallel to a considerable degree of accuracy because they were lapped together and held accurately parallel for all positions of the upper electrode by the differential-screw assembly. The faces of the sample were also parallel in most cases since materials which were readily machinable were faced in a lathe. Materials such as glass and quartz came to us in sheet form and it was found that their surfaces were parallel.

In reply to Doctor Yager's comments, the authors wish to thank him for pointing out the important reference, the paper by Mr. D. L. Hollaway, in which paper Mr. Hollaway describes a resonant-cavity "Dielectrometer." The authors at the time of writing this present paper were, unfortunately, not aware of Mr. Hollaway's paper which was published in a journal which does not receive very wide circulation.

Although the resonant cavity described in the authors' paper embodies the same principle of a re-entrant coaxial cylinder, which was used by Mr. Hollaway, there are many important differences between the two instruments, some of which differences Doctor Yager noted in his comments.

The frequency at which Mr. Hollaway used his Dielectrometer was at the lowest part of the region for which the present cavities were designed. The use of an auxiliary micrometer capacitor to detune from resonance at the higher frequencies in which these cavities are used is certainly open to question. The inductance in such an auxiliary capacitor certainly would have an appreciable effect for which a correction would have to be made. Mr. Hollaway also used a diode detector and this is much more troublesome than a crystal attached to a probe at these higher frequencies. A considerable part of Mr. Hollaway's paper is devoted to considering the errors and correcting for the errors in his diode voltmeter. No difficulty at all was encountered with the very simple crystal detectors attached to a galvanometer or microammeter.

Although the differential-screw assembly used in this apparatus to move the electrode has proved satisfactory, the expense of machining it has led the authors on more recent designs of these cavities to use a commercially made precision micrometer head instead. The use of a reduction-drive device to turn the micrometer, together with a large dial mounted on top, has been found to permit sensitive enough tuning to determine the Q of the empty cavity by susceptance variation in the main electrode.

Doctor Englund's method of measurement, which Doctor Yager referred to, was used by him at 1,300 megacycles and is applicable at the higher end of the range for which these cavities can be used readily. Its applicability and sensitivity decreases as the wave length increases, however, since the sample, unless it is increased in length as the wave length is increased, will occupy a decreasing fraction of the field in the coaxial line. It is also more difficult to prepare a sample to fit well in a coaxial tube. This is especially true of ceramics and certain plastics. Because of the ease of preparing the samples and the extremely simple calculations involved, the authors feel that the resonant-cavity method as it has been described here is especially suited to the frequency range of 100 to 1,000 megacycles. Other methods might be preferable at the lower and higher ends of this region.

In regard to the question of metallic losses in the cavity, raised by Mr. Larrick, it should be noted that in all the methods given by the authors for determining tan δ, the dissipation factor of the cavity alone is separated from the dissipation factor of the cavity plus the sample so that metallic losses in the walls of the cavity do not affect the accuracy of tan δ of the dielectric sample. However, it is desirable to keep the metallic losses as low as possible in order to keep the Q as high as possible. A high Q is desirable so that low-loss materials may be measured with reasonable accuracy. Many low-loss materials have a Q of several thousand or more at these frequencies, and if the measuring device had a Q of only several hundred the additional loss due to the presence of such materials could not be detected. A measured Q of over 3,000 has been obtained from some of these cavities and this value might be increased by the use of more sensitive detectors.

As pointed out by Mr. Zimmermann, this method, because of the ease of preparing the sample, the speed of measurement, and the simplicity of the calculations, is well suited to the production control of dielectric materials.

This apparatus could also be used conveniently to measure the attenuation of coaxial cable.

Development of Excitron-Type Rectifier

Discussion and author's closure of paper 44-78 by H. Winograd, presented at the AIEE North Eastern District technical meeting, Boston, Mass., April 19–20, 1944, and the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944, and published in AIEE TRANSACTIONS, 1944, pages 969–78. The first discussion was presented at St. Louis; the second at Boston.

J. H. Cox (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): I am most pleased that Mr. Winograd's paper points out the basic superiority of the single-anode tube over multianode construction, even in the large-size units. This makes unanimous agreement on that point among the builders of mercury-arc rectifiers in America. I have thought for a long time that the early rectifier engineers were misled by the fact that most electric apparatus became more attractive as the size of units was increased and continued to struggle for more anodes in larger units even though it was recognized that an increase in size of unit increased losses. Of course, a balance must be struck between efficiency and the complication of multiplicity of parts. However, Mr. Winograd has demonstrated that, even with the continuous-excitation principle, by better engineering of details and of auxiliary apparatus the complication can be minimized to a point where the sacrifice demanded by single-anode construction is well justified by the efficiency gain and the simplification of the main arc carrying features.

In any given type of arc rectifier, the losses go up with increase in size, so even with single-anode construction the current per anode should not be increased indefinitely. At the present time, we are building ignitron tubes, six of which have a nominal rated load current of 5,000 amperes at 600 volts. However, this size is most attractive for large installations requiring a large number of anodes. It is doubtful if larger tubes will be desirable until some further basically superior principle is developed.

Mr. Winograd reports that his designs *B* and *C* failed to perform satisfactorily. These designs had no solid baffle between cathode and anode. Tanberg and others at the Westinghouse Research Laboratories found that the cathode spot of an arc in a vacuum emits a high-velocity highly ionized jet of vapor. We have found invariably that if this jet is permitted to impinge on the anode, the arc-back probability is greatly increased. I believe that this effect is the compelling reason for a baffle between cathode and anode rather than the effect of radiated heat from the anode increasing the vapor pressure, although the latter has some effect.

The use of inverse current as a criterion of rectifier arc-back quality has long been considered and, although it is admittedly imperfect, I am glad that Mr. Winograd has used this system in extensive tests. As he pointed out, the measurement of inverse current in high-current rectifiers is very difficult and the values given in the paper are probably considerably in error due to the indefinite value of back current in the diverter rectifier and the influence of the measuring equipment on the back current of the rectifier under test. The relative values, however, could be quite useful.

I want to emphasize the fact that all arc-back causes may not be influenced by inverse current, although this may well be the predominant type. There is considerable data to show that, while a large portion of arc-backs occurs at transition, many also occur later in the inverse-voltage period and it does not seem certain that many of these later arc-backs are due to so-called "mechanical" causes. Illustrative of the difficulty of determining the causes of arc-back is a recent experience in which it was finally established that about 90 per cent of the arc-backs of a satisfactory group of rectifiers was caused by the splashing of mercury drops when liquid mercury returned to the vacuum chamber from the vacuum connection, which was inadequately baffled. Until the real cause was established, the picture was quite confused. Although other methods yield valuable information, I do not believe that we have arrived yet at a point where we have a complete substitute for a reasonably long-time load test to determine the quality of a rectifier; the final determination of its quality being dependent upon field experience.

Various uses are made of grids in rectifiers and these uses have influence on the grid design and the losses involved. At voltages up to 900 the grid in an ignitron is used primarily for deionization and thus the design of the grid is determined by the requirements to reduce arc-back frequency to a satisfactorily low level. In the excitron, even at low voltages, the grid must also be adequate for phase control of voltage. To provide adequate phase control at high loads, a tighter grid is necessary than would be needed for deionization alone. This, of course, means some increase in arc-drop voltage.

A continuous-current arc of the order of one to three amperes may be quite stable by itself in a usual rectifier tube. However, in a single-anode rectifier, a large pulsating current to the main anode is superimposed on the excitation current. At the end of the main-anode conducting period the tank is filled with highly ionized gas and momentarily the excitation anode can be supplied with the required electron current from the gas space while it is being deionized. This tends to permit the cathode spot to disappear and for a given main-anode current the excitation current must be increased from the minimum to avoid extinction. Mr. Winograd mentions an excitation current of seven to eight amperes. Is this adequate for any more than normal full load? On railway service where overloads as high as 200 per cent above rated full load are often required, must the excitation current be further increased? Also, is the excitron left extinguished and in need of restarting by a bus or feeder short circuit?

C. C. Herskind (General Electric Company, Schenectady, N. Y.): Mr. Winograd is to be complimented on the thorough and frank treatment of the construction, mode of operation, design problems, and performance characteristics of the excitron rectifier presented in his paper. The paper provides a description of the developmental procedure typical for mercury-arc rectifiers and adds further background useful in the current revision of rectifier standards.

Two basic problems arise in the operation of a single-anode rectifier with a continuous-excitation arc. The first is that of limiting the range of the cathode spot to the surface of the mercury pool, preventing its establishment on the tank wall. Some form of insulation between tank wall and cathode is, therefore, essential. The other problem is the prevention of extinction of the cathode spot at the end of the conduction period. The high residual ionization in the plasma provides a source of electrons for a short interval at the end of conduction. This electron current does not terminate at the cathode spot on the surface of the mercury pool and if this current is appreciable the cathode spot may be extinguished. This shunting current which flows to the tank wall may be reduced in actual magnitude by reducing the area of the part of the tank exposed to ionization and not insulated from the cathode. This is, therefore, another reason for insulating the tank wall from the cathode. Despite such insulation, there is some excitation-arc current which does not terminate at the cathode spot and so the excitation arc must be maintained at a current value sufficient to prevent extinction under overload and fault conditions where the intensity of ionization is high.

It would be of interest to know the minimum excitation current at which an excitron may be operated under usual service conditions without trouble due to cathode spot extinction, and also what troubles have been experienced in this connection.

The principal differences between the ignitron and excitron rectifiers are the excitation circuit and the cathode insulation required in the excitron. The typical firing circuit for a six-anode ignitron requires a space of approximately two feet by three feet by five feet and consumes from 100 to 400 watts per ignitor. It may be either tube-fired or of the magnetic type using metallic rectifiers. Phase control is obtained by varying the saturating current in the magnetic-firing circuit or by means of a phase-shift transformer. The grids of the ignitron are excited from 110-volt source through 500-ohm resistors and consume very little energy. The firing circuit for an excitron uses a simpler d-c excitation system but requires a better excitation system for controlling the grids inasmuch as the grids provide the phase-shift control. The paper indicates that the firing circuit required for the excitron is comparable with that of the ignitron regarding number of components, size, and power requirements. With the necessity for providing some type of insulated cathode in the excitron tank, there does not seem to be any appreciable advantage of one type over the other.

The conclusion that the presence of an excitation arc during the inverse period does not affect the rectifier capacity as determined by arc-back to any appreciable extent is supported by tests on ignitrons. A standard ignitron tank has been operated with the igniter firing twice during the cycle, once at the start of conduction and a second time 180 degrees later, during the inverse period. The ignitor current was approximately 40-amperes crest and of 15-degrees duration, and its presence did not reduce the rectifier capacity as limited by arc-back. Observations of grid currents in a standard ignitron have shown that ionization exists throughout the inverse period.

The desirability of measuring the inverse current in mercury-arc rectifiers has been recognized, but little work has been done because of difficulties in obtaining absolute measurements. Mr. Winograd's data confirm these conclusions. The method which he describes appears to provide a means of comparison but, while useful in checking progress during the development of a particular design, is not suitable for determining relative capabilities of designs which differ markedly.

In describing the inverse voltage immediately following conduction two suggestions are offered. First, the definition of the initial inverse voltage as the voltage obtained at the first instant during the inverse period, assuming the rectifier circuit to contain inductance only with no distributed capacity. This is the value usually calculated and the one shown in Figure 11 of Mr. Winograd's paper. Since actual rectifiers have distributed capacity in the various parts of the anode circuit, a high-frequency transient voltage may appear following current zero in the same manner as in the circuit breaker. This voltage is superimposed on the initial inverse voltage and the term "recovery voltage" has been applied to it in the circuit-breaker studies. It is also suggested that the comparison of the initial inverse voltage obtained on different circuits would be facilitated by expressing the initial inverse voltage in terms of the theoretical direct voltage instead of the crest value of the anode to neutral voltage.

H. Winograd: Mr. Cox agrees that the baffle has the dual function of shielding the anode against the mercury vapor from the cathode and shielding the cathode against the heat of radiation from the anode, but believes the former is the more important function. Since the two functions are concurrent and interrelated their relative importance is a matter of conjecture. The tests on designs *B* and *C*, Figure 15 of the paper, indicate that the effect of heat radiation from the anode to the cathode is by no means negligible.

I readily agree with Mr. Cox and Mr. Herskind in regard to the limitations of the inverse-current test method. As I have stated in the paper, this method can be used only for purposes of comparison and is not a substitute for extended load runs. However, in spite of its limitations, this test method is very useful for evaluating and predicting the effects of design changes and of different operating conditions on the arc-back performance of a rectifier. The particular usefulness of this method in the development of rectifiers is that quantitative factors of comparison are obtainable from relatively short load tests, which is not possible with the accelerated-load test method, except for the count of the occasional arc-backs.

Mr. Cox questions whether the inverse current is the only cause of arc-backs. At high direct voltages, the peak inverse voltage on an anode may be a greater cause of arc-backs than the inverse current. There are undoubtedly other causes of arc-backs of an "accidental" or "mechanical" nature. However, it has been observed by most investigators that in the medium and lower ranges of direct voltages the majority of arc-backs occur at the conclusion of the firing period and the inverse current is believed to be the chief cause. The splashing of the mercury from the vacuum connection toward the anode, mentioned by Mr. Cox, might have accounted for many of the arc-backs that occurred later in the inverse-voltage period.

Mr. Cox and Mr. Herskind have raised a question in regard to the stability of the excitation arc at the conclusion of the firing period of an anode during overloads or short circuits. Artificial arc-back tests were made on excitron units in which the other anodes carried high forward current without losing their excitation arcs. Artificial arc-through tests were made recently on an excitron in an inverter circuit in which the anode carried high forward current without extinction of the excitation arc. These tests indicate that the excitation arc is stable at high forward-load currents. The excitation current during these tests was about eight amperes.

Mr. Herskind's comparison of the ignitron and excitron designs and their excitation circuits overlooks important factors which influenced the decision to develop the excitron. In the excitron, a continuous arc is maintained at the cathode by means of a graphite excitation anode, and this arc is unaffected by the position of the excitation anode, the mercury level in the cathode, or by turbulence of the mercury. In an ignitron, the arc at the cathode is started during each cycle. An ignitor of special material has to be immersed in the mercury to the correct depth. Its operation may be influenced by turbulence or impurities on the surface of the mercury, and deposition of metal vapor makes it inoperative. The ignitron is subject to occasional failures to start the arc at the cathode, called "misfires." The limited reliability of the ignitor is indicated by the fact that sealed-off ignitrons are provided with two or three ignitors and a holding anode.

While the excitron requires two additional seals for the cooling coil, the ignitron has a rather complicated adjustable seal for the ignitor, to permit external adjustment of its position in the mercury.

The excitation circuit of the excitron is considerably simpler than that of the ignitron as can be seen by comparing Figure 5 of the paper with any of the published ignitor firing circuits. The d-c excitation power for the excitron can be obtained from any available supply and is practically unaffected by single-phase system disturbances because it is supplied from a three-phase source. The ignitor must be supplied with a peaked current of proper wave shape with the correct phase relation to the voltage of the main anode.

I have compared the various points of the two types of single-anode rectifiers to indicate that the development of the excitron was motivated by a desire for simplification. I have no intention of minimizing the accomplishment of the engineers who developed the ignitron. They have used considerable ingenuity in overcoming handicaps and their success is attested by the successful operation of a large number of ignitron units.

Lighting Required for Commercial-Airline Aircraft

Discussion of paper 44-91 by W. A. Petrasek, presented at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944, and published in AIEE TRANSACTIONS, 1944, August section, pages 593–8.

P. H. Greenlee (nonmember; first lieutenant, United States Army Air Corps, Wright Field, Dayton, Ohio):

Position Lights

The system of position lights used by the military services is essentially the same as that shown in Figure 1 of the paper, except that in many military aircraft the candlepower of the tail light is somewhat lower than that used by commercial aircraft. Passing lights have been used by both commercial and military aircraft and are still used in training types of military aircraft. These passing lights increase the visibility of the airplane when viewed from straight ahead. The light is a small red spotlight mounted in the leading edge of the left wing and is intended chiefly for use along the airways or in areas of heavy traffic. More adequate wing-tip lights would permit complete elimination of passing lights.

More recent types of military aircraft have the wing-tip light mounted within a plastic inclosure, which is streamlined into the wing tip. This eliminates the protrusion of the wing-tip light.

The Army Air Forces now are flashing position lights on certain types of training and cargo aircraft; however, no added lights have been standardized by military services, although in some cases certain lights have been added by local organizations.

Landing Lights

Both leading-edge type and retractable landing lights have been used by the military service. The Army Air Forces prefer the leading-edge type, because less trouble has been experienced with that type than with the retractable type largely because of its simplicity. Although actual lamp changing takes longer, the total maintenance is less with the leading-edge type of installation. Improvements in retractable-lamp assemblies undoubtedly will change this situation. One other consideration in favor of leading-edge installation is its lighter weight.

The Army Air Forces' practice is to install two landing lights on two-pilot airplanes, and a single lamp on the left wing of the single-pilot airplane. A small number of Army Air Forces aircraft have had the landing light mounted in the wheel well. The meager experience on this installation indicates that satisfactory operation is obtained.

Taxiing Lights

The Army Air Forces now are beginning to use separate taxiing lamps on some airplanes. In these initial installations a 50-watt all-glass lamp $4^{1}/_{2}$ inches in diameter is used. This lamp has a beam candlepower of 5,000, a horizontal spread of 40 degrees, and a vertical spread of five degrees. Initial experiments indicate that airplanes can be taxied reasonably well with this lamp. The number of lamps installed is determined by the size and span of the airplanes.

Double-filament landing lights are under consideration; however, the aiming of the taxiing beam will, of necessity, be dependent upon the aiming of landing beam, and it is probable that when the landing beam is aimed correctly the taxiing beam will not be aimed correctly in all cases. In any case the experience of the Army Air Forces indicates that the wattage should be considerably lower than that of a landing lamp, because, when an airplane is taxiing, the generators may not be charging and the high wattage of the landing lamp imposes a heavy drain on the battery. In addition an airplane can be taxied with much less light than is required for landing.

Cabin Lighting

Basically, cabin lighting in military aircraft is intended to provide general area illumination. Individual crew positions are provided with small flexible spotlights or floodlights with red filters to meet the needs of the individual position. These lights are designed to meet the military needs and usually are provided with dimming rheostats.

Cabin lighting of military aircraft can be regarded as entirely utilitarian. The attractiveness and eye appeal desirable in commercial airplanes need not be carried to so high a degree of development in military aircraft.

Cockpit Lighting

As Mr. Petrasek has mentioned, military aircraft have used very widely, the ultraviolet-fluorescent system of instrument lighting. Some difficulties have been encountered through the malplacement of the ultraviolet sources. Considerable interest is being shown in the military services in an indirect lighting system using red light. This system of course is somewhat more complicated than the ultraviolet, but some of the problems in the malplacement of the fixtures can be eliminated. A small incandescent cockpit lamp for general use in the cockpit is installed in each airplane. This lamp can be adjusted to either a small spot of light or a wide beam; it is controlled by a rheostat, and a red filter is provided.

Indicator Lights

Another serious problem in aircraft lighting is that of indicator or warning lights. The problem is to design the light so that it will be bright enough to give a positive signal in bright sunlight and still provide a means of dimming for night operation to avoid blinding the pilot. The Army Air Forces have used two dimming devices to obtain this result. One is a small hinged cap with a small hole in it so that the cap can be turned over the lens during night operations; the other is a rotating cap with two disks resembling a salt-shaker top.

One of the newer type indicator lights includes a built-in test switch so that the pilot can check readily to see if the bulb is burned out.

General

Mr. Petrasek has stated very clearly many of the problems pertaining to present-day aircraft lighting. Generally speaking, the needs of military aircraft are almost identical with those of commercial aircraft with the possible exception that cabin lighting in military aircraft may be regarded from a purely functional viewpoint.

Gaseous-Discharge Lamps for Airplane Lighting Service

Discussion and author's closure of paper 44-136 by E. W. Beggs, presented at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944, and published in AIEE TRANSACTIONS, 1944, October section, pages 760–2.

J. T. Gove (Los Angeles, Calif.): Fluorescent lamps now are used for instrument lighting only on Army aircraft and on Naval aircraft of older design and not on all military aircraft as implied in this paper. On Naval aircraft of new design, red indirect lighting is used for instrument panels, and red lighting also is used for general cockpit illumination and for chart-board illumination. The reason given for the changes to red lighting is that it is a means for providing better dark adaptation of pilots' eyes. Whether red lighting or fluorescent lighting best accomplishes this purpose is apparently a controversial issue.

It is believed that fluorescent lamps operating directly from a 24-volt d-c system are to be preferred over the types operating from a 110-volt 400-cycle system. The primary reason for this preference is that the 110-volt lamp requires the installation of an inverter on an airplane having a 24-volt d-c system, which means highly undesirable additional weight.

Fluorescent lamps operating directly from 24-volt d-c systems have been tested and found satisfactory and now are installed on several Army aircraft. To the best of our knowledge, these systems are performing satisfactorily.

W. H. McCandless (major, United States Army Air Corps, Wright Field, Dayton, Ohio): As Mr. Beggs indicates, fluorescent lamps have been used on military aircraft for ultraviolet instrument lighting with rather good experience regarding starting and output. After starting at low temperatures the ultraviolet output is low but is still high enough to make the instrument characteristics visible. This apparently is caused by the ultraviolet output of the argon arc. In a few minutes the ultraviolet output rises to nearly normal, probably because of the fact that the inclosing fixture allows the tube to come up to nearly normal temperature. Tubular fluorescent lamps have been used as a source of visible lighting for the navigator's work table in a few airplanes but were replaced by incandescent lamp fixtures, because the application required a lamp which could be dimmed by a rheostat. Other than this the Army Air Forces has had no experience with fluorescent lamps in the cabin.

By far the great majority of fluorescent lamps now in service in military aircraft are the 28-volt d-c *RP*12-bulb type. The experience with the initial low ultraviolet output at low temperatures and subsequent increase of output is about the same with these lamps as with tubular fluorescent lamps.

The Army Air Forces has had practically no experience with glow lamps on aircraft, but they do appear to offer good possibilities as indicator lamps on airplanes using 115-volt or higher electrical systems.

E. W. Beggs: The Army experience reported by Major McCandless confirms our understanding that low temperatures are not a serious handicap for small fluorescent lamps used in instrument lighting because of the enclosing fixtures used. For cabin lighting, which is the principal use considered in the paper, an enclosing fixture, therefore, should provide adequate protection if, needed against low light output with low ambient temperatures.

In reply to Mr. Gove, the 24–28-volt direct current would be rather inefficient for general lighting service with fluorescent lamps. The arc stream must be somewhat elongated or the electrode losses become too high in proportion to the total wattage consumed in the lamp. This consideration requires higher voltages and also favors 400-cycle alternating current. The low-voltage direct current is suitable for instrument lighting, because the total wattage required is small and consequently the absolute losses are negligible.

Aircraft-Engine Torque Instruments

Discussion and authors' closure of paper 44-138 by F. W. Godsey, Jr., and B. F. Langer, presented at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944, and published in AIEE TRANSACTIONS, 1944, September section, pages 686–90.

J. A. Townsend (engineering division, Wright Field, Dayton, Ohio): The electric torque instrument of whatever type appears to be at a disadvantage in its present state of development, because of inherent installation, calibration, and maintenance difficulties, at least for use on production airplanes. While the hydraulic torque instrument is also susceptible to improvements, particularly in low-temperature operation and in overcoming torsional vibration with resulting erratic indications of the pointer, continued development would appear to have more promise of success than with an electric type.

Hydraulic torque instruments have been reduced in weight and cost and are easier to calibrate for production engines than an electric type, as described in the paper, where a careful and laborious calibration on each separate engine apparently would be necessary. It is understood that in many cases hydraulic torque instruments now in use can be used to calibrate the dynamometer stand equipment. In other words, more consistent and dependable horsepower values may be obtained with the hydraulic torque instrument than with the dynamometer.

F. W. Godsey, Jr., and B. F. Langer: Mr. Townsend's unfavorable criticism of electric torque instruments as compared with hydraulic torque instruments ignores several of the facts of the case. The most obvious one is that the hydraulic torque instrument can be applied only to engines having the planetary type of gear reduction. No one yet has devised a hydraulic torquemeter for use on engines with spur gear reductions. Such engines comprise a considerable percentage of those now built for aircraft use.

As far as ease of calibration is concerned, the advantage is with the electric type and not the hydraulic type. The Magnetic-Coupled Torquemeter can be calibrated statically with the engine not running, and in many cases not even present. The calibration of the hydraulic torque instrument requires that the oil pump be operated to supply oil pressure to the hydraulic system.

The electric torque instrument is comparatively new and requires further refinement to improve its accuracy and dependability, but the writers believe that the inherent advantages are in its favor. The problems which remain in its development are ones which require only refinement of design, whereas, the difficulties experienced with the hydraulic torque instrument are of a more fundamental nature.

Transient Performance of Induction Motors

Discussion and authors' closure of paper 44-175 by F. J. Maginniss and N. R. Schultz, presented at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944, and published in AIEE TRANSACTIONS, 1944, September section, pages 641–6.

C. Concordia (General Electric Company, Schenectady, N. Y.): This paper presents data of considerable interest not only in connection with performance during jogging and plugging, as mentioned by the authors, but also for other applications. For example, electric-power-system load characteristics during transient voltage disturbances are of importance in determining system behavior. Since induction motors may form an appreciable part of the system load, it becomes of interest to know the response of such motors to sudden changes in terminal voltage as given, for example, in Figures 4 to 7 of the paper. Another case mentioned briefly in the paper is that of two motors on the same shaft. In this case it may be possible in transferring from the larger to the smaller motor to close the line switch on the smaller motor, when it is running considerably above its synchronous speed. The case of pole changing in a single motor is similar, but the two-motor case is emphasized here because the drive shaft for the small motor may be smaller than that for the large motor, and

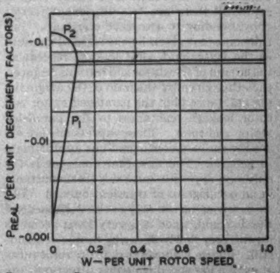

Figure 1. Decrement factors as functions of rotor speed (real parts of roots)

Induction motor

$X_1 = X_2 = 0.08$

$X_m = 3.00$

$r_1 = r_2 = 0.01$

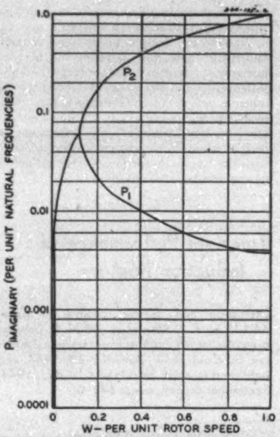

Figure 2. Natural frequencies as functions of rotor speed (imaginary parts of roots)

Induction motor

$X_1 = X_2 = 0.08$
$Xm = 3.00$
$r_1 = r_2 = 0.01$

thus the shaft torques may be more significant.

A glance at Figures 8 and 9 of the paper reveals that maximum torque is obtained with the motor at standstill (run 90, Figure 8) and that the torque oscillation apparently is increasing. Actually of course the torque oscillations eventually will decrease but will have a component with a very small decrement. As a partial explanation of this phenomenon Figures 1 and 2 may be of interest. These figures show the decrement factors and natural frequencies of an induction motor having constants almost identical with those of the machine studied by the authors plotted against rotor speed. Figure 1 of this discussion shows that at standstill there are two decrement factors corresponding to two time constants: one time constant given by the ratio of the sum of the stator and rotor leakage reactances to the sum of the stator and rotor resistances, the other given by the ratio of the magnetizing reactance plus the paralleled stator and rotor leakage reactances to the paralleled stator and rotor. These expressions are exact for the case considered but approximate in general. The first time constant is the only one apparent (as a d-c time constant) in an oscillogram of transient current. The second is apparent only in the decrement of the flux and, since it is very long, accounts for the very slowly decaying torque at standstill. Figure 1, shows that, if the rotor turns at even a low speed (in either direction), the long (flux) time constant decreases rapidly, so that one would expect the transient torques to decay more rapidly, when the motor is running. At the same time the d-c (current) time constant is increasing until at about 12 per cent the two time constants are equal. They are equal because the stator and rotor resistances and leakage reactances are equal. In general, with the motor running there is a stator d-c time constant equal approximately to the ratio of the transient reactance (short-circuit or standstill reactance) to the stator resistance and a corresponding rotor d-c time constant approximately equal to the ratio of the transient reactance to the rotor resistance. The difference between the apparent time constants at standstill and full speed has, of course, been known for a long time; the calculations reported here were made in order to show the continuity of the characteristics as functions of speed and to determine the lowest speed at which the full-speed concepts may be used.

Figure 2 of this discussion shows that, although we have referred to the time constants as d-c time constants, this concept is also only approximate. Actually, if the motor is running at above about 20 per cent speed, there are two natural frequencies: one very nearly equal to rotor speed and the other extremely small. The stator current has a d-c component which decays with the stator d-c time constant and a speed-frequency component which decays with the rotor d-c time constant. Similarly the rotor current has d-c and speed-frequency components which decay with the rotor and stator d-c time constants, respectively. At very low speeds, however, both components are of exactly one-half-speed frequency in the present case.

G. Angst (General Electric Company, West Lynn, Mass.): The results presented in this paper must be of particular interest to the application engineer. Through them a deeper insight has been gained into the actual performance of polyphase induction motors on many industrial applications. For instance motors operating punch presses, rapid reversing motors for drilling and tapping, certain motors for tire-making machines required to perform many jogging operations in rapid succession, and loom motors, to name only a few, belong to this class.

In punch-press applications the steepness of the slope of the speed-torque curve of the driving motor near synchronous speed for years has been a controversial subject. It appears, as a result of this paper, of little consequence in many cases whether a high-slip or a low-slip motor is selected, because the initial rate of change of transient torque is zero at the instant when the punch hits the work piece, regardless of the slope of the steady-state speed-torque curve. The amount of rotor resistance may influence the duration and the peak values of the transient torques and currents, and more data is required to evaluate its importance. In jogging and reversing applications of a severe nature a better knowledge of the magnitude of the transient currents enables the engineer to arrive nearer the optimum motor design for the job at hand.

It is hoped that the authors will widen the scope of their investigation to include the effect of changes in motor-design constants on transient torques, currents, and speeds. A beginning in this direction has been made as shown in Figure 15 of the paper. It is interesting to note that the transient torques of the motor with high-rotor resistance are about twice as severe as in the motor with low-rotor resistance. On the other hand the authors have pointed out that low-resistance motors in general tend to accentuate transient phenomena, particularly if trapped rotor flux is present. Clearly an extension of the study along the direction just outlined is desirable.

There is one factor not specifically mentioned in the paper which very likely affects torque transients to a considerable degree. This is what is commonly called stray-load loss. It is well known that the true steady-state speed-torque curve differs appreciably from the curve based on the equivalent circuit, particularly on small motors. This differential torque is due to stray-load loss and may be considered as a load on the motor. Per unit it is proportional to between the 1.5th and second power of the speed and proportional to the second power of the rotor current. During braking and generator action it is additive, and during normal motor operation it is subtractive. In a typical rapid-reversing motor this additional torque, at full speed backward, is of the same order of magnitude as the torque determined from the equivalent circuit. It is believed that the influence of stray-load loss on torque transients cannot be neglected, if a true picture of the phenomena is to be obtained.

F. J. Maginniss and **N. R. Schultz:** The authors wish to thank Mr. Concordia and Mr. Angst for their interesting comments.

In reply to Mr. Angst's discussion, the authors hope to undertake further investigations of motor transients. Such investigations logically would include the effect of various machine parameters, as well as stray-load loss on transient performance.

The Nature of Vibration in Electric Machinery

Discussion and author's closure of paper 44-177 by Troy D. Graybeal, presented at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944, and published in AIEE TRANSACTIONS, 1944, October section, pages 712–18.

Quentin Graham (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): It may be noted that there is one source of electromagnetic force that Professor Graybeal has not emphasized. If the center of the rotor travels in a small circle around the center of the stator, such as would occur with a bent shaft, there is a point of minimum air gap which travels at rotor speed. As this coincides with the maximum magnetic field at twice slip frequency, the characteristic beat wave is accounted for without the necessity of a second harmonic in the mechanical force wave. The test shown in Figure 3 of the paper is, of course, evidence that a second harmonic mechanical force did occur in the tested motor, which would be accounted for by keyways or other dissymmetries. However, it is my belief that in many cases a bent shaft alone is sufficient cause. This does not mean, necessarily, a permanent distortion of the shaft, but rather a bending within the elastic limit as a result of the centrifugal force of a slightly unbalanced weight accentuated by the increased magnetic pull on the side having the minimum air gap. I should like to ask the author whether he has experience that may confirm or disprove this point of view.

C. G. Veinott (Westinghouse Electric and Manufacturing Company, Lima, Ohio): Professor Graybeal has presented a commendable analysis of a particular problem. It may be, however, that he underestimates the importance of dynamic balance, since he states that "components of vibration having frequencies which are multiples of the rotational speed cannot be reduced appreciably" by improving the dynamic balance. Some experimental evidence to the contrary has been observed by the writer.

During the course of a noise investigation, a rubber-mounted single-phase 60-cycle 1,725-rpm motor was mounted on a sheet-steel panel. The rubber mounting effectively isolated the normal 120-cycle torque pulsation, so that there was no audible 120-cycle hum emanating from the panel. When a small weight of modeling clay was placed on the shaft extension, a distinct low-frequency audible hum was detected. By means of a noise analyzer it was determined that the domiant frequency was 150 cycles, or the fifth harmonic of the rotational frequency. This 150-cycle hum could be obtained or eliminated simply by applying or removing the weight on the shaft extension. While the 150-cycle component was the principal one, an additional component of approximately 1,000 cycles accompanied it; the 1,000-cycle component also could be reproduced or eliminated at will by the addition or removal of the unbalance on the shaft extension. Hence, it must be concluded that dynamic unbalance sometimes may set up vibrations which are harmonics of the rotational frequency.

Professor Graybeal's analysis is for a polyphase induction motor. Single-phase induction motors frequently have slip-frequency pulsations, and these are one of the most annoying sources of noise. Slip-frequency modulation of a bearing noise causes a noise similar to a cat purring. Magnetic noise, too, is often modulated at slip frequency; such magnetic noise includes the slot-frequency components.

Professor Graybeal measures natural frequencies by striking the object with a rubber hammer. Another method of providing an exciting force is by means of a small d-c motor with flywheel and unbalanced weight. The motor is rigidly attached to the test member; exciting frequency is controlled by motor speed, and exciting force is controlled by the size of the unbalance weight on the flywheel. Vibration is picked up by means of suitable pickups. This method provides a continuous exciting force. It has been found most useful in vibration studies on aircraft generators, for example.

It is to be hoped that Professor Graybeal will extend the scope of his investigation to the study of slip-frequency pulsations in single-phase induction motors.

Troy D. Graybeal: The author is grateful to Quentin Graham and C. G. Veinott for their comments and discussion. It is impossible in a short paper such as this to cover every known aspect of vibration in electric machinery. Comments which bring out points in addition to those covered in the paper are most welcome.

As mentioned by Mr. Graham, a bent-shaft or an off-center rotor would result in an electromagnetic driving force in addition to those mentioned in the paper. This particular type of dissymmetry would produce a component of vibration having a frequency twice the slip frequency of the motor, since the minimum air gap would coincide with the magnetic field twice during each revolution of motor slip. If this component of vibration were present together with one of comparable magnitude due to the mechanical unbalance, the resultant vibration would be of the form:

$$V = A \sin 4\pi Nt + B \sin 4\pi\, st$$

where A and B are the maximum amplitudes of the two corresponding components, N is the speed of rotation, and s is the slip, both measured in the same units, such as revolutions per second. This equation is plotted in Figure 1 of this discussion with $A = B$, $N = 59$ revolutions per second, and $s = 2$ revolutions per second. The "character" of the vibration is quite different from that displayed by the oscillograms of the paper, as the wave is periodically displaced, rather than modulated, by the slip-frequency component. Because of nonlinearities in the final vibrational response in an actual machine, some modulation might take place, but the main effect is a periodic displacement of the wave. If the distortion of the shaft is a varying proposition within the elastic limit of the shaft, the coefficient B would be a variable, and the low-frequency component would appear and disappear alternately if the rate of flexure were low. If rapid, the rate of flexure would no doubt adjust itself to coincide with one of the natural resonant frequencies of the shaft and rotor assembly. This phenomenon, of course, is that usually referred to by the name of "critical speeds" and is covered in the paper.

Mr. Veinott points out that slip-frequency modulation is a very annoying type of vibration in single-phase motors. Such components can be detected in almost every single-phase machine. The practical answer has been to keep these components of vibration to a low enough level so they will not be serious. The factors discussed in the paper in connection with three-phase machines also would apply to single-phase machines. But this does not provide the whole answer. There are other factors which are of perhaps even greater importance. These have been associated in a general way with the elliptic nature of the revolving magnetic field inherent in a single-phase motor. While this general association is probably correct, it needs considerable investigation to explain the mechanism of just how the slip-frequency modulation of the resultant machine vibration takes place.

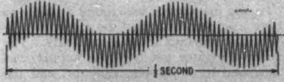

Figure 1. Slip-frequency vibration of an electric motor

The question raised by Mr. Veinott regarding the statement in the paper "components of vibration having frequencies which are multiples of the rotational speed . . ." actually boils down to the meaning or thought the author wished to convey. The author has found among certain groups an "idea fixe" to the effect that the matter of dynamic balance is the whole story as far as excessive vibration in electric machinery is concerned. It was to these groups that this statement was directed in the hope that it might explode this belief and make way for a realization that there are other factors that frequently must be considered on a par with the matter of proper balance. As worded, the statement does perhaps need additional explanation. The author is aware that harmonic components of vibration sometimes decrease very greatly in amplitude with an improvement of dynamic balance. These cases, however, are in the minority as far as the author's experience is concerned. This leads to the belief that in such cases there are agencies in addition to dynamic unbalance that give rise to the harmonic components of vibration, but that they arise in such a manner as to be accentuated by a condition of dynamic unbalance. Thus there would be present more than one agency acting cumulatively to produce the resultant vibration. In such cases, the vibration could be reduced by attention to any of the responsible agencies provided they could be isolated. But to isolate these different agencies may not be an easy matter. If it is found, as in the case outlined, that improvement in balance will reduce the component of vibration to a negligible value, then of course that is the practical answer to the particular problem.

Design Considerations for D-C Aircraft Generators

Discussion and author's closure of paper 44-241 by J. D. Miner, Jr., presented at the AIEE Los Angeles technical meeting, Los Angeles, Calif., August 29–September 1, 1944, and published in AIEE TRANSACTIONS, 1944, volume 63, pages 1234–40.

J. E. Mulheim (nonmember; Westinghouse Electric and Manufacturing Company, Lima, Ohio.): Calculated frequency-amplitude curves such as that shown in Figure 10 of Mr. Miner's paper serve several useful purposes. Qualitatively, they indicate the critical frequencies of the generator structure at which any appreciable excitation will produce destructive amplitudes of the various structural components. Quantitatively, in regions other than those where the structure is resonant, they can be expected to give reasonable estimates of the forces acting on the different members when used in conjunction with engine-vibration curves.[1] Their principal disadvantage is that they are not applicable in the three regions of resonance because the effects of damping are not considered. The calculations required to incorporate the effects of damping are very arduous. However, it is possible to determine the more or less exact shape of such curves by an electrical-analogy method.[2]

The constants used in these calculations were for an early model of the *P*-1 generator; they are representative of similar generators of like size and design. With a given mass and inertial distribution, the first critical frequency is primarily a function of the stiffness of the mounting flange and engine rear cover. Similarly, the second and third critical frequencies are, primarily, functions of the commutator end bracket and armature stiffness, respectively. The stiffness of the armature is difficult to measure consistently since it is influenced by the commutator, shaft, punchings, conductors, varnish, insulation, and so forth. The stiffness of the armature was estimated for the purposes of these calculations.

Experimental evidence has been obtained which verifies the first two critical frequencies; they are substantially those predicted in the curves. The maximum operating speed of the test equipment, 50,000 cycles per minute, was not great enough to reach the third critical frequency; readings obtained at 50,000 cycles per minute indicated that it was being approached and would be at about 55,000 cycles per minute. This difference between calculated and measured frequency is probably representative of the variations in armature stiffness due to the factors mentioned above.

The curves indicate that there are definite possibilities of exciting the last two critical frequencies as well as the first. Under such conditions, the deflection of the commutator end bracket can be much greater with less excitation than at the first critical frequency. Most engine-vibration studies have not included measurement at these higher frequencies, yet recent flight studies on generators indicate that excitation at these higher frequencies can result in very high accelerations.[3]

REFERENCES

1. AIRCRAFT-ENGINE-ACCESSORY VIBRATION, John Tyler. AIEE TRANSACTIONS, volume 63, 1944, pages 1334–40.

2. ELEMENTS OF MECHANICAL VIBRATION (book), C. R. Freberg, E. N. Kemler. John Wiley and Sons. First edition, pages 153–69.

3. AIRCRAFT-ELECTRIC-ACCESSORY-VIBRATION INVESTIGATIONS, D. R. Miller. AIEE TRANSACTIONS, volume 63, 1944, pages 1283–90.

J. D. Miner, Jr.: Mr. Mulheim's discussion shows that the day of trial-and-error design of generators is passing rapidly. Experimental work now in progress should make it possible to predict the loads which will occur in the critical members of generator structures at various frequencies and exciting amplitudes and to check the fatigue endurance of these members when used on various engines by comparing laboratory tests of generator components with test readings or engine amplitudes.

One of the most widely used combinations of generator and engine is the *P*-1 generator on the Pratt and Whitney *R* 1830 engine. Reference to Figure 3 of Mr. Tyler's paper "Aircraft Engine Accessory Vibration" shows that the *R* 1830 engine has a single-amplitude excitation of 0.006 inch at 2,500 rpm, corresponding to a two-and-one-half order frequency of 6,200 cycles per minute. Reference to Figure 10 of the paper shows how narrowly this engine frequency misses the first generator-mounting-pad critical frequency occurring just below 8,000 cycles per minute. Worse yet, the full military climb rating for the engine occurs at about 2,500 rpm, or at a two-and-one-half order frequency of 6,250 cycles per minute. A slight increase in generator mass, such as might be required to obtain a lower minimum operating speed, would be likely to result in exact coincidence of generator and engine critical frequencies at the full-power rating of the engine.

Little can be done to raise the generator critical frequency so long as present mounting standards must be maintained. Even if the generator designer can avoid any alteration in a successful generator design, it is quite possible that a change in the engine or in the propeller may result in coincidence of critical frequencies and an epidemic of generator failures.

One promising development now undergoing preliminary tests consists of a tuned vibration damper. A simple experimental model has shown an ability to reduce vibration amplitudes to 40 per cent of the value obtained without damping. A refined design of this vibration damper is expected to weigh approximately one pound.

Carbon-Pile Voltage Regulators for Aircraft

Discussion and author's closure of paper 44-184 by W. G. Neild, presented at the AIEE Los Angeles technical meeting, Los Angeles, Calif., August 29–September 1, 1944, and published in AIEE TRANSACTIONS, 1944, November section, pages 839–43.

Ernest B. Evans (nonmember; Lockheed Aircraft Corporation, Burbank, Calif.): In addition to the applications for voltage regulators discussed by Mr. Neild, one other may be of interest.

In one instance it was found possible to drive a separately excited d-c motor at substantially constant speed at all loads. The motor generator set used for supply was controlled by a voltage regulator, and the terminal voltage was caused to rise with load by connecting a shunt in the negative line and connecting the equalizer coil across it. The voltage rise was adjusted to equal substantially the *IR* drop in the motor armature. The motor speed was therefore substantially constant at all loads.

Moreover the motor speed was found to be adjustable over a wide range by connecting various numbers of battery cells in series with the voltage coil on the regulator. By this method the voltage applied to the motor armature was readily adjustable, and no other adjustment was necessary to maintain the proper voltage rise with load.

W. G. Neild: The application described by Mr. Evans is another example of the many novel and useful applications to which carbon-pile regulators may be applied.

Mr. Evans states in the last paragraph of his discussion:

> "Moreover the motor speed was found to be adjusted over a wide range by connecting various numbers of battery cells in series with the voltage coil on the regulator. By this method the voltage applied to the motor armature was readily adjustable, and no other adjustment was necessary to maintain the proper voltage rise with load."

It should be pointed out that by rewinding the voltage coil with fewer turns of larger size wire the regulated voltage can be lowered to any minimum value desired. Inserting resistance in series with the coil would cause the regulated voltage to increase. In this way the coil could be wound so that the regulated voltage would be the minimum required and could be raised to any value by a manual rheostat, thus giving a smooth even adjustment, and the batteries could be eliminated. If the Eclipse model 1042 is the regulator referred to, this could be accomplished simply by replacing the 32-ohm fixed resistor with an adjustable rheostat, and it would not be necessary to rewind the voltage coil. The desired value of regulated voltage could be obtained readily by adjustment of this rheostat.

A 40-Kva 400-Cycle Aircraft Alternator

Discussion and authors' closure of paper 44-190 by H. E. Keneipp and C. G. Veinott, presented at the AIEE Los Angeles technical meeting, Los Angeles, Calif., August 29–September 1, 1944, and published in AIEE TRANSACTIONS, 1944, November section, pages 816–21.

W. A. Barden (major, United States Army, Wright Field, Dayton, Ohio): The alternator described by the authors is representative of current design trends as regards electric generating equipment for aircraft. While the machine has the lowest weight per unit output, approximately 2.8 pounds per kilowatt, of any generators or alternators so far developed for use in aircraft, it does not represent the ultimate attainable.

However, it is believed that the aforementioned weight is amazingly low when the overload requirements imposed on the design are considered and the conservative mechanical design which has resulted in a truly rugged piece of electric equipment.

The extent to which the rotor will have to withstand torsional vibration when driven by a constant-speed drive is not known. However, tests of the alternator while being driven directly by a step-up gearbox attached to a nine-cylinder aircraft engine indicate that the design adequately provides for this consideration.

Since the alternator was designed for a synchronous speed of 6,000 rpm, it is questionable whether it ever will be successfully driven directly attached to an auxiliary engine. It is believed that a satisfactory solution is possible if it is driven through a gearbox and a coupling device capable of damping out torsional vibration. However, such a solution probably would be undesirably heavy.

The cooling problem was one of the most difficult of all those encountered in the design of the alternator. Limitations on weight and size of the alternator, together with the restrictions imposed on the amount of cooling air to be made available for cooling the alternator, are but mildly indicative of the magnitude of the problem. It is believed that a great amount of work remains to be done in the field of blast cooling aircraft electric equipment. Not only should the work be done, but the science of blast cooling should be firmly established, and suitable literature is needed for reference in connection with the design of new electric equipment. A very important part of this work would consist of evaluating the advantages to be gained from blast cooling of electric equipment, compared with the disadvantages to the aircraft due to increased drag. Certainly a great amount of information is needed in order to make possible the best solution to any problem concerning blast cooling of electric equipment.

The performance curves shown in Figure 13 of the paper are indicative of the excellent results obtained by the designers of the alternator. The authors of the paper have done a commendable job in relating the history of the development of a 40-kva 400-cycle aircraft alternator.

W. L. Berry and **J. P. Dallas** (Hughes Aircraft Company, Culver City, Calif.): Arbitrary figures have been used in Table I of the paper which are incorrect. (They seem to have been so chosen as to favor greatly the 208/120-volt three-phase system.) The following comments are offered with the purpose of correcting the errors in Table I.

1. The alternator weight is listed as 80 pounds in Table I and 85 pounds in the first paragraph of the conclusions, but actually weighed 90 pounds when delivered.

2. A 30-kw 120-volt d-c generator weighed 79 pounds and two 15-kw units have a total weight of 106 pounds. However, 120 pounds is shown for this generator in Table I.

3. If all of the factors are considered, it is probable that the weight for remote-mounting and driving an 80-pound machine will exceed considerably the 15 pounds listed in Table I. This additional weight is eliminated when two main engine-mounted 15-kw generators are used.

4. An unidentified constant-speed drive is listed in Table I at 90 pounds, but at present the only delivered unit weighs 100 pounds and requires a five-inch oil cooler weighing an estimated ten pounds, with an additional eight pounds of oil making a total weight of 118 pounds.

5. The very fact that an oil cooler is required with the constant-speed drive indicates considerable power loss resulting in higher fuel consumption. If a generally accepted figure of one-half pound of fuel per horsepower per hour is used, and a constant-speed drive efficiency of 85 per cent is assumed, the additional fuel for constant-speed drive based on full load for ten hours or 50 per cent load for 20 hours amounts to 35 pounds per drive.

6. In Table I it is not apparent why 120 pounds is shown in the d-c column and 100 pounds in the a-c column for the weight of 28-volt power components. There is good reason for having six kilowatts of direct current available on the a-c airplane to satisfy the solenoid, relay, clutch, and control-circuit loads, but it is possible and desirable to put all these loads on the 120-volt d-c system.

7. The requirement of Table I for two 12-kva single-phase 400-cycle power sources in addition to the 120-volt d-c system seems unjustifiable. An eight-engine airplane $2^1/_3$ times the weight of the one mentioned in this paper having an a-c autopilot and a-c instruments requires only three inverters of three-fourths kilovolt-ampere for total a-c power and standby requirements.

8. In view of the foregoing Table I should be revised as indicated in Table I of this discussion.

For further comparison of a-c and d-c electric systems refer to AIEE technical paper 44-238, "Higher-Voltage D-C Aircraft Electric Systems."[1]

Conclusion. A saving of 660 pounds should justify the use of 120 volt direct current in preference to 208/120-volt alternating current. These figures show the a-c system to be twice as heavy as the equivalent d-c system. In fact why is alternating current proposed for aircraft electric power?

REFERENCE

1. HIGHER-VOLTAGE D-C AIRCRAFT ELECTRIC SYSTEMS, W. L. Berry, J. P. Dallas. AIEE TRANSACTIONS, volume 63, 1944, November section, pages 843–9.

Table I

	Components	120-Volt D-C System	208/120-Volt 400-Cycle 3-Phase System
1.	Generator	106	90
2.	Voltage regulator	12	6
3.	Main - generator breaker or switch	12	6
4.	Drive shaft and mounting		30
5.	(*a*). Constant-speed drive		100
	(*b*). Oil cooler		10
	(*c*). Weight of oil		8
6.	Total items 1–5	130	250
7.	Total weight of four units	520	1,000
8.	Additional fuel for constant-speed drive based on a full load for ten hours or 50 per cent load for 20 hours (two units only)		70
9.	Two 28-volt 6-kw d-c power sources		100
10.	Two $^3/_4$-kva single-phase 400-cycle power sources at 17 pounds	34	
11.	Four sets of synchronizing controls		40
12.	Weight of main-power wiring	55	65
13.	Total weight excluding utilization equipment and wiring	609	1,275

S. R. Bergman (General Electric Company, Lynn, Mass.): Personally, I am very much interested in this paper pertaining to a three-phase alternator rated 40 kva, power factor 75 per cent, 400 cycles, 6,000 rpm, because we have designed and built machines of the same rating. I would, therefore, like to make the following comments:

The most important consideration in all parts going into an airplane is weight. I am rather disappointed in the weight of 85 pounds, quoted in the paper, since from our experience it can be made 10 to 12 pounds lighter.

The authors have not commented on the ability of their alternator to supply unbalanced loads. While the majority of loads will be balanced, it is probable that certain single-phase loads will be present on the system. While these loads normally will be distributed on the three phases, due to their intermittent and variable nature, some unbalance will exist. It would be interesting if the authors would give some information on the degree of unbalanced load which this alternator can supply without dangerous overheating.

Concerning the mechanical features, I note that both the exciter and the revolving field are mounted on a single shaft. For a machine of this size, I think it is better to have a detachable exciter driven through a quill shaft; then, whatever vibrations there exist in the main generator are to a large extent eliminated from the commutator of the exciter which is important for obtaining long brush wear. Another advantage of such a construction lies in the fact that the exciter can be replaced easily if out of order, and any part of the machine can more readily be repaired.

The comparison between the polyphase system and the 120-volt d-c system is interesting but not conclusive in showing the a-c system to be lighter. The difference shown is so small that it could easily be thrown the other way by refinements in design. More important, however, is the character of the utilization apparatus and the control and protective equipment, the weight of which has not been evaluated.

Furthermore, in the a-c system, we meet some very difficult problems, such as that of running a number of alternators in multiple, as well as the problem of distributing the current equally between the generators. This leads to a complicated control system of not inconsiderable weight. In the d-c system these problems already are solved. D-c motors also have better torque characteristics than induction motors and can be made as series, compound, or shunt motors for different applications. The polyphase motor does not possess these good torque characteristics and suffers accordingly.

I have discussed at some length the comparison between the polyphase system and the 120-volt d-c system, because I believe personally that, if the polyphase system should not materialize, then we can always fall back on the 120-volt d-c system. One of the main factors which originally forced consideration of alternating current for certain high-altitude aircraft was the question of commutation and brush wear at high altitude on d-c generators and motors. In recent years this problem has received a great deal of attention, and much progress has been and is being made toward a satisfactory solution.

H. E. Keneipp and **C. G. Veinott:** The authors wish to thank Major Barden for his constructive comments on our paper. His comments as to auxiliary engines are probably meant to imply that it is doubtful that 6,000-rpm auxiliary engines ever will be built. If such an engine were built, there is no reason why this alternator could not be direct-driven by it. Regarding cooling, Major Barden is right, and considerable attention was paid to this problem.

Berry and Dallas have confined their discussion to the relative merits of alternating versus direct current. This was a subordinate issue in the paper which mainly deals with a description of a 400-cycle alternator for a-c aircraft systems. The authors had no intention of trying to settle the relative merits of these two systems, and the introductory comments were intended only to show some of the reasons why this alternator came to be developed. It was stated that the figures given in Table I of the paper were developed by a group of engineers and not by the authors of the paper. As pointed out in the paper, the figures in Table I were presented before the Dayton Section of AIEE by the group of engineers listed. Prior to the meeting, however, the table had been developed by a larger representation of engineers. These individuals included engineers experienced with all three systems and the data in Table I are, in the authors' opinions, more accurate than the revised figures given by Berry and Dallas.

It must be pointed out also that the weights given in these tables do not necessarily refer to specific apparatus which is now available, but represent weights which a group of machine designers believed could be met to build apparatus to the specifications outlined for this study. It is interesting to note that, whereas Berry and Dallas believe that a weight of 90 pounds is necessary, S. R. Bergman thinks that it can be done for 73 to 75 pounds. The machine described in the paper actually weighed 85 pounds, and its weight can be reduced by redesign.

The weights given by Berry and Dallas for 15-kw and 30-kw d-c machines are interesting, but we doubt very much that they will develop these ratings under the conditions outlined for Table I: that is, 3,000-rpm minimum operating speed, and 50 per cent overload capacity for five minutes. A 15-kw engine-mounted 3,000-rpm d-c generator with an overload capacity of 50 per cent for five minutes weighing only 53 pounds would be better than any now available. As to the weights of the constant-speed drive, the figure given was based on combined judgment as to what is expected to be achieved and took into consideration more than one proposed drive. Higher efficiency of the alternator as compared to the d-c generator at least will compensate partially for the losses in the constant-speed drive although the d-c generator would have a higher efficiency than the combination of alternator and drive.

In answer to Berry's and Dallas' question, the weight of a motor generator with an a-c driving motor is less than the same set with a d-c driving motor, which explains the difference in weight in line 9 of Table I. Berry and Dallas question the amount of 400-cycle power required on this airplane. The amount of power required is specified by the Army Air Forces for a military airplane and would not be used in a commercial airplane such as described by Mr. Berry. Consequently, the amount of power was a matter of specification by the Army, and not judgment by the engineers who prepared the table.

Doctor Bergman is to be thanked for his comments. Obviously he and Berry and Dallas do not agree as to the weights of a 40-kva alternator. As pointed out in the paper, we too believe that the alternator can be built for less than the weight given in the paper which was an actual weight. However, we did not wish to duplicate the unfortunate experiences of early aircraft generators which were all originally built too light, thereby sacrificing strength in order to reduce weight at the expense of reliability. In this connection, we are quite surprised to see Doctor Bergman advocate spindle-drive when so much difficulty has been experienced in the field with broken spindles on d-c aircraft generators. His comments on the separate exciter are interesting, particularly since our first preliminary design utilized a separate exciter, but, after further study, this design was abandoned in favor of the integral construction reported in the paper, primarily because it was believed that a single two-bearing unit was more rugged, more reliable, and inherently lighter in weight, than a two-machine construction.

Doctor Bergman asked for information on the degree of unbalanced load which this alternator can supply without dangerous overheating. This subject could well form the basis of an AIEE paper in itself. A number of unbalanced load tests have been taken on this alternator, and every indication is that this alternator will withstand the same per cent unbalance as a conventional 60-cycle machine.

The authors agree 100 per cent with Doctor Bergman that the comparison between the polyphase system and the 120-volt d-c system is not conclusive, because it was not intended to be so. The final conclusion of the engineers who prepared this table agrees remarkably well with Doctor Bergman's comments, namely, that it probably would be the utilization equipment which would determine the better system. This still leaves the problem as to which is the better of the two systems, a question which must be determined by careful engineering analysis of the use for which the airplane is intended, the equipment used on it, the type of mission for which it is intended, and numerous other factors.

Blast-Tube Cooling for Aircraft Generators

Discussion and author's closure of paper 44-84 by Cyril G. Veinott, presented at the AIEE North Eastern District meeting, Boston, Mass., April 19–20, 1944, and at the AIEE Los Angeles technical meeting, Los Angeles, Calif., August 29–September 1, 1944, and published in AIEE TRANSACTIONS, 1944, July section, pages 520–5; this discussion presented at the Los Angeles technical meeting.

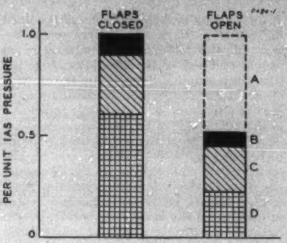

Figure 1. Generator blast-cooling pressure and losses

Flaps on oil cooler and intercooler
IAS—Indicated air speed
A—Apparent loss in induction system up to generator blast-tube inlet
B—Tube loss (calculated for $Rg=3.5$)
C—Accessory-compartment pressure
D—Generator inlet pressure

G. G. Setterlund (nonmember) and **R. R. Mille** (Westinghouse Electric and Manufacturing Company, Lima, Ohio): Mr. Veinott is to be congratulated for developing a simple means of handling this vital subject. There is a great need for more specific information on blast tubes for cooling aircraft generators. This need was first emphasized when the early development of high-altitude brushes was delayed because such information was not available.

Our early chamber tests of altitude treatments of aircraft-generator brushes were taken at the specified ten inches of water total air pressure across the generator. Satisfactory performance in the chamber was not duplicated on flight tests. Investigation then revealed that, instead of ten inches of water total pressure across the generator, there was only about four to six inches of water total pressure at slow flying speed. Chamber tests at six inches then were made and the results compared favorably with the flight tests. Six inches pressure across the generator was then adopted as the standard for chamber tests.

These chamber tests had been taken in the altitude chamber at the United States Bureau of Standards at Washington, D. C. Altitude conditions could not be duplicated exactly in this chamber. When the high-altitude chamber was installed at Lima, equipment for duplicating altitude conditions was provided. The first objective was correlation of the chamber-test data with flight-test data taken by United States Army Air Forces.

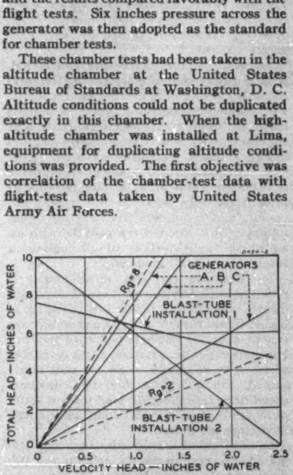

Figure 2

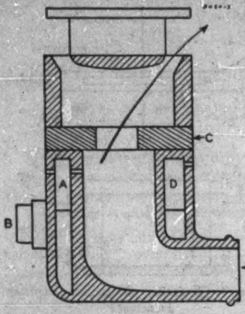

Figure 3. Orifice for generator-blast-cooling pressure measurements

Orifice	Equivalent Generator Resistance
1	2
2	8

A—Orifice pressure-drop transmitter
B—Standard generator connector—pressure transmitters connected to terminals
C—Removable orifice
D—Accessory-compartment pressure transmitter
E—Air inlet

In order to obtain varied correlation data, sample generators were supplied from Wright Field so that generators *A*, *B*, and *C* (letters correspond to generators described in Mr. Veinott's paper) could be tested in the chamber and the results compared with flight tests. With six inches of water total pressure across the generators the results obtained from generators *A* and *B* agreed quite well with flight tests, but generator *C* ran much cooler than on flight tests. The large difference in pressure drop in the blast tube for different values of air flow was then recognized. When an allowance was made for this in a manner similar to that in Mr. Veinott's paper, the chamber-test results agreed very closely with flight-test results.

The flight pressure for each type of generator then was calculated from the blast-tube characteristics obtained from flight tests and used for chamber tests. The standards used for our chamber testing agree very well with curve 3 of Mr. Veinott's paper. Test results obtained check flight-test results very well.

This clearly points out the need for more complete blast-tube characteristics when designing and testing aircraft generators. We urge that the recommendation in Mr. Veinott's paper regarding specifications for blast tubes be followed. With definite specifications for the blast-tube characteristics, both the air-frame manufacturer and the generator manufacturer would know definitely what is required. The generator designer could use this information to obtain the maximum output with the ventilation available. Tests could be made with the ventilation that would be obtained on the airplane, and flight performance could be predicted by tests in chambers which are capable of duplicating the altitude conditions. This would help prevent misapplication of generators and obtain more reliable electric systems. We suggest that the blast-tube characteristics of present airplanes, and especially of experimental airplanes, be obtained and made available to generator manufacturers so they could take full advantage of the ventilation available and avoid overheated and burned-out generators.

Harvey J. Finison (Wright Field, Dayton, Ohio): Mr. Veinott succeeds in greatly simplifying many of the usual concepts of blast cooling. By the use of electrical analogue he adds to a better understanding of the problems of blast cooling, especially for the electrical engineer. Judicious design of blast-cooling installations should make possible an increase in pressure available for cooling of aircraft generators. This will also be evident as the minimum operating speeds of military aircraft continue to increase. Material saving in weight for a given generator capacity or, more probably, considerable increase in capacity for a given weight will be a direct result.

It is possible that the paper somewhat overemphasizes the importance of the resistance of the blast tube itself in limiting the pressure available at the generator inlet. In most installations the pressure is limited a far greater amount because of the location of the blast-tube inlet in such a manner as to receive only a small part of the total pressure available because of the velocity of the airplane.

The result of flight measurements on one airplane which has the generator blast-tube inlet connected to the intercooler duct is shown by Figure 1. For the most unfavorable conditions with the oil-cooler and intercooler flaps open, it may be noted that of a 78 per cent loss in air-speed pressure the resistance of the generator blast tube itself accounted for only five per cent loss. Unfortunately, examples such as this are fairly typical. In order to improve such an installation, long blast tubes leading to a higher pressure area might be necessary. Then the tube resistance may become the limiting consideration.

While closed-end and open-end measurements on the blast tube may be satisfactory for defining blast-tube characteristics, the aircraft designer should not be required to obtain a specific closed-end pressure. For generator *A*, Figure 2, either blast-tube installation 1 or 2 would be satisfactory. Installation 1 will be far better for any generator of lower resistance. Yet this installation would be excluded by the specification proposed by Mr. Veinott because of lower closed-end pressure. A more suitable specification would be as follows:

> The blast-tube shall be designed to deliver for any conditions of flight a total pressure head at the entrance to the generator of at least eight inches of water when connected to a generator or equivalent device having an air resistance of 8, and at least five inches of water when connected to a generator having an air resistance of 2. Air resistance of the generator is defined as the ratio of total head to velocity head.

The use of the pitot tube as a measuring device in blast-cooling tests presents some problems of installation and may be of questionable accuracy especially where only a short straightening section preceded by curves can be used. The pitot tube has been used successfully in flight measurements on a number of airplanes, but on others even the insertion of the short section proposed by Mr. Veinott would be impossible. A more satisfactory method would be to replace the generator with a measuring device such as shown by Figure 3. Electrical pressure transmitters could be used to eliminate the need for running tubing to the cockpit for pressure indicators. Measuring instruments would be connected at the voltage-regulator base which is normally in an accessible position. This method of course is applicable only where the particular generator removed is not essential to local flight.

C. G. Veinott: Setterlund and Miille have described very well the situation and the need which prompted the investigation and analysis reported in this paper. A prime object was to dispel some of the mysteries surrounding the mechanics of how blast cooling worked and to establish a common viewpoint and a common language for discussion of the subject. Such groundwork as this is absolutely necessary in order to permit an accurate interchange of information between the air-frame builders, the procuring agencies, and the accessory manufacturers, and such interchange of information is generally necessary before really useful specifications can be written. Some of this groundwork even now is bearing fruit in new standards which are being worked up by an Society of Automotive Engineers committee. Setterlund and Miille are to be thanked for pointing out some of the difficulties met when there was no such general understanding of the subject.

Mr. Finison is probably right in his point that the paper appears to overemphasize the importance of the resistance of the blast tube. However, as he recognizes, the paper aims mainly to set the form of the specifications rather than to set quantitative values, and the importance of tube resistance is probably better brought out by slight overemphasis. However, if one refers to Figure 4 of the paper, it may be well to point out that curve 3 commonly was felt to represent the blast-tube characteristics of one four-engine airplane, and that is the curve now being used by Setterlund and Miille for altitude-chamber testing. Curve 4 of Figure 4 was sketched in arbitrarily to show the effect of a tube having less closed-end pressure and less resistance; curiously enough, later flight tests on a different four-engine airplane showed the characteristics of its blast tube to agree surprisingly well with curve 4.

Mr. Finison suggests defining in the specifications only a part of the pressure–volume curve of the blast tube in order to allow greater latitude to the air-frame designers. In principle, I must most certainly agree with him in allowing as much latitude as possible to the air-frame builders. However, use of open- and closed-end pressures results in a specification which is simpler to write and to use; until sufficient data can be taken to establish whether or not it will impose a hardship on air-frame builders, this simpler form of specification should be considered as a possibility.

Mr. Finison proposes the use of two standard calibrated orifices to measure two points on the pressure–volume curve. It is necessary to measure only two points, any two points, to establish the entire pressure–volume curve. By far the simplest and easiest point to measure on this curve is the closed-end pressure, which does not require the measurement of air volume. A large orifice, as proposed by Mr. Finison, probably would be a more accurate way of measuring a second point on the pressure–volume curve. Or the dummy generator might have a straight section of pipe with air straighteners and a pitot static tube near its exit. The advantages of open-end and closed-end readings should not be overlooked, even if only a part of the pressure–volume curve is made a part of the specification.

Functional Design of Aircraft Electric Actuator Equipment

Discussion of paper 44-201 by C. E. Gagnier presented at the AIEE Los Angeles technical meeting, Los Angeles, Calif., August 29–September 1, 1944, and published in AIEE TRANSACTIONS, 1944, November section, pages 813–15.

Ernest B. Evans (nonmember; Lockheed Aircraft Corporation, Burbank, Calif.): Mr. Gagnier has ably treated the factors involved in the design of aircraft electric actuators. Much might be gained by the interchange of ideas and information regarding design details. Gear train arrangements which allow maximum manufacturing tolerances, and production methods by which semiskilled personnel may hold those tolerances are especially to be sought. The same may be said of clutch-brake design. In one actuator of recent design it was found possible to improve the magnetic circuit and reduce the accumulation of tolerances, and yet simplify production and testing of the actuators by combining a steel coil housing, a brake face, and a guide sleeve into one part. The coefficient of friction between the clutch faces was increased considerably by radial rather than annular grinding of the driving face.

Design Considerations of 400-Cycle Aircraft Motors

Discussion of paper 44-210 by M. B. Sawyer, Sr., presented at the AIEE Los Angeles technical meeting, Los Angeles, Calif., August 29–September 1, 1944, and published in AIEE TRANSACTIONS, 1944, December section, pages 877–9.

W. A. Barden (major, United States Army, Wright Field, Dayton, Ohio): This paper presents a very clear-cut picture of the requirements which have to be met, not only by the airplane designer, but by the accessory designer as well. It is the responsibility of the accessory designer to meet these requirements to the greatest degree. Likewise, it is the responsibility of the airplane designer to select accessory equipment which meets these requirements to the greatest degree.

The comparison of the performance of a given motor on 60-cycle power with that of the same motor on 400-cycle power accounts for the very considerable weight saving which is possible with high-frequency power. Even though certain compromises have to be made in the design of a motor to operate on 400-cycle power, we still have the advantage of a more powerful motor for the same weight or a motor of the same power for much less weight than is possible using 60-cycle power.

In order to retain the fundamental advantages of the squirrel-cage induction motor, it is unlikely that such devices as a centrifugal switch will be resorted to in order to attain the best combination of starting torque, full-load torque, power factor, and so forth.

However, it is quite probable that these motors will be used with centrifugal clutches in order to take advantage of desirable running characteristics obtained with motors having poor starting torque.

It is believed that the weight saving resulting from the use of high-speed motors driving through gear reductions has been clearly established and the practice will be seen to increase, especially in the application of induction motors to aircraft.

A New High-Frequency Capacitor

Discussion and author's closure of paper 44-209 by W. M. Allison and N. E. Beverly, presented at the AIEE Los Angeles technical meeting, Los Angeles, Calif., August 29–September 1, 1944, and published in AIEE TRANSACTIONS, 1944, December section, pages 915–16.

S. W. Zimmerman (General Electric Company, Pittsfield, Mass.): In introducing a capacitor for specific application to high-frequency problems, the authors undoubtedly are helping to focus attention on the limitations of standard designs of components when applied to high-frequency problems. Problems of high-frequency by-pass encountered in many places, particularly in radio-noise suppression, require lower impedance devices than offered by standard ratings and constructions.

An early solution of high-frequency by-pass capacitor design in early cathode-ray oscillograph measuring technique,[1,2] as well as in communication problems, was a four-terminal network (Figure 1 of this discussion) which enabled the designer to eliminate the inductance and radio-frequency resistance of the leads or bushing to the active part of the capacitor. In general, however, for the range of frequencies considered in this paper, this design is inadequate and more satisfactory characteristics can be obtained from an exposed foil roll with double tap straps such as shown in Figure 2, the axis of the roll being vertical in this case. This design becomes a three-terminal network in certain installations where the outer foil is solidly fastened to ground. Such a design has enabled the designer to provide more capacitance and thus obtain lower impedances at the lower frequencies without sacrificing high-frequency performance.

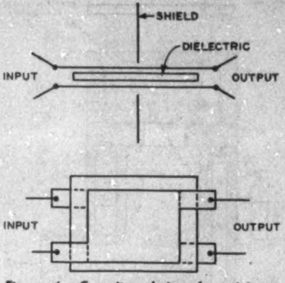

Figure 1. Capacitor design for minimum inductance

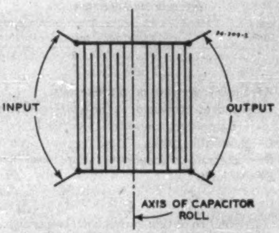

Figure 2. A design for low inductance and higher capacitance

The Hypass construction shown in Figures 1 and 2 of the paper is a reasonable compromise between these two designs but introduces two factors which may or may not be considered desirable from the design standpoint. First, the foil is required to carry power current, and second, the effective length of ground lead changes with frequency. The first of these factors results in heating the foil and directly raising the temperature of the dielectric. The second of these factors results in a change of effective capacitance with frequency, due to a change in current distribution as higher frequencies are applied. This mechanism is consistent with the authors' remarks in the second paragraph under the heading "Performance."

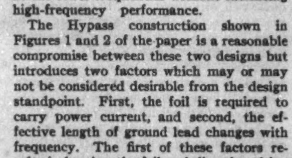

Figure 3. Through-stud "pyranol" capacitor, rated 0.55 microfarad, 0.100 ampere, 250 volts maximum (alternating current or direct current), for radio-noise suppression

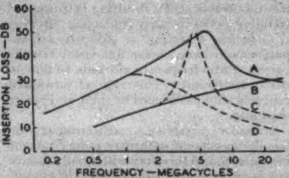

Figure 4. Comparative characteristics of conventional and special capacitor designs

Curve *A*—Through-stud type 0.55 microfarad
Curve *B*—Hypass 0.1 microfarad
Curves C and D—Conventional designs

There has been available for some time a through stud-type capacitor which has been designed for efficient radio-noise filtering over the frequency range of 1.5 to 20 megacycles and which is also effective at the higher frequencies. This unit shown in Figure 3 of this discussion consists of a through study having a body diameter of $^5/_{16}$ inch, around which is wound the capacitor foils and dielectric. Foils are staggered so that an edge of one foil is exposed at each end of the winding. The case of the unit is connected to one foil and is grounded through a two-hole mounting bracket. In this design the foil itself carries only radio-frequency current so dielectric heating due to I^2R loss in the foil is avoided. This unit having nominal capacitance of 0.55 microfarad is rated 250 volts alternating current or direct current and will carry 100 amperes continuously with adequate terminal connections. A characteristic attenuation curve for this unit is plotted, together with the information contained in the authors' paper, as Figure 4. A consideration of these curves will show the regions in which each of these units is most effective.

The through-stud-type capacitor is available as a component suitable for radio-noise suppression and other by-pass applications where frequencies in the region of 1 to 30 megacycles or higher require special consideration. Aircraft applications have favored this capacitor because of reduction in weight in comparison to conventional filters of like current rating.

References

1. Surge Phenomena (book) 1933–1940. The British Electrical Allied Industries Research Association, page 149.

2. Surge Propagation, T. F. Wall. *Engineering* (London, England), volume 153, April 17, 1942, page 301.

R. J. Colin (lieutenant, United States Army, Wright Field, Dayton, Ohio): The radio-noise suppression field has always needed a capacitor which would actually have capacity reactance at frequencies as high as 50 megacycles.

The subject paper is, to my knowledge, the first to describe such a noninductive-type capacitor. If these capacitors perform as well as claimed by the subject paper, they will be quite useful for noise-suppression work.

W. M. Allison: The Hypass construction is not a compromise between the two types of construction indicated by Mr. Zimmerman as Figure 1 and Figure 2. The Hypass is unique in that resonance effects are entirely absent due to the distribution of the constants of the device, whereas the designs cited by Mr. Zimmerman would all show resonance characteristics, say, below 30 megacycles for capacities greater than 0.25 microfarad. In the Hypass construction there is no evidence of resonance at least as high as 40 megacycles for capacities in this range.

The Hypass type of construction can be made so that the foil carries only a small fraction of the power current, thus removing the first of Mr. Zimmerman's undesirable design factors. We cannot see how the second of Mr. Zimmerman's undesirable factors will cause any difficulty in the use of Hypass capacitors.

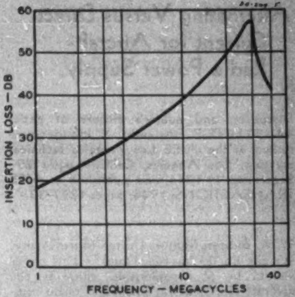

Figure 5. 0.14-microfarad feed-through type, 100 amperes, 250 volts, alternating current or direct current

We also make a through-stud, also called feed-through-type, capacitor which, while not a Hypass, offers effective attenuation over the range of 1 to 40 megacycles as shown in Figure 5. This capacitor is rated at 100 amperes, 250 volts alternating current or direct current. By means of this design, the series inductance is reduced to a low value. There is still, however, sufficient inductance to resonate with the capacity.

The capacitor shown by Mr. Zimmerman in his discussion owes its characteristics partly to the electrical design and partly to the fact that his use of polar dielectrics causes the capacity to drop off with frequency in this range. Thus the resonance is broader than would obtain with the nonpolar dielectrics used in the embodiments of the Hypass shown.

Alternating Versus Direct Current for Aircraft-Radio Power Supply

Discussion and author's closure of paper 44-180 by D. E. Fritz and C. K. Hooper, presented at the AIEE Los Angeles technical meeting, Los Angeles, Calif., August 29–September 1, 1944, and published in AIEE TRANSACTIONS, 1944, pages 1227–33.

W. A. Barden (major, United States Army, Wright Field, Dayton, Ohio): This paper should be of considerable value to the aircraft electrical engineers and radio engineers in determining the effects of power supply on the weight of radio equipment. The advantages and disadvantages of both a-c and d-c power have been listed very carefully and are of quite general interest.

However, it is believed that the radio (or for that matter, all electronic) loads do not constitute a sufficiently large proportion of total electric load to warrant a choice of electric power system based on the requirements of optimum performance—weight characteristics of this type of equipment. There will always be airplanes for which each of the present-day standard systems of 6, 12, or 24 volts direct current is best suited when all factors have been considered.

It is believed to be equally true that from now on there will be airplanes for which 115 volts or 230/115 volts direct current or 208/120-volt 400-cycle three-phase electric systems will provide the lowest weight of over-all electric installations.

This leads to the conclusion that factors such as aircraft weight, range, and total electric generating capacity can be combined into a simple formula, the solution of which is the optimum voltage for the aircraft under consideration. A comparison of the weights of wire of generating equipment for d-c and a-c systems indicates that the optimum voltage for an a-c system is approximately twice that for a d-c system to do the same job. Reference is made to Table I of AIEE technical paper 44-190[1] in which it is shown that the weights of wiring and generating equipment for the 120-volt d-c and the 208/120-volt a-c systems for the same job are essentially equal. Thus, it is necessary to consider other factors in order to arrive at a choice between direct current and alternating current. Once this has been done the optimum voltage then can be determined by considering the factors previously indicated.

REFERENCE

1. A 40-KVA 400-CYCLE AIRCRAFT ALTERNATOR, H. E. Keneipp, C. G. Veinott. AIEE TRANSACTIONS, volume 63, 1944, November section, pages 816–21.

D. E. Fritz: The authors thank Major Barden for his discussion of their paper, and we believe he has brought up several interesting points. Major Barden is of the opinion that the radio and electronic equipment on any given airplane do not constitute a large enough proportion of the total electric load to be a prime consideration in determining the type of electric system for the airplane. The authors agree with Major Barden in this matter but believe that the problem is more complex than evidenced by Major Barden's discussion. Very few large airplanes have but a single electric system. Almost all of the large military airplanes have equipment that must be operated from alternating current, even though the main electric system is 27.5 volts direct current. There has been a tendency for aircraft electrical engineers to compare the aircraft electric system to a public-utilities power system. However, it would be better possibly to compare the aircraft electric system with the electric system for a manufacturing plant, where both a-c and d-c power are available, in order to provide for the special applications that will not utilize alternating current efficiently. Since both a-c and d-c power are available on most large airplanes, the type of power supply chosen for the radio equipment should be that which will result in the best factors of weight, reliability, and so forth. For large aircraft this will not result in the addition of a separate electric system to operate the electronic equipment, but will merely mean that the radio will be operated from the power most adaptable to its requirements.

In many cases alternating current may be used to great advantage, even though separate alternators need be provided on airplanes having a d-c system. The alternating current could be provided from combination a-c and d-c generators, as mentioned in the paper.

The choice of voltage does not greatly influence the radio equipment, if some means of conversion equipment is used to obtain the high-voltage plate-supply power. Unless electronic tubes are developed that will operate on the low direct voltages of d-c aircraft systems, some sort of conversion means will be necessary.

Radio-Noise Elimination in Military Aircraft

Discussion of paper 44-213 by G. Weinstein, H. H. Howell, G. P. Lowe, and B. J. Winter, presented at the AIEE Los Angeles technical meeting, Los Angeles, Calif., August 29–September 1, 1944, and published in AIEE TRANSACTIONS, 1944, November section, pages 793–5.

S. W. Zimmerman (General Electric Company, Pittsfield, Mass.): The authors have done a splendid analytical job in the problem of radio-noise treatment in aircraft. This subject brings into play all the imagination and resourcefulness of the engineer.

In evaluating the effectiveness of noise-elimination means the authors consider receiver and source filtering as well as compromise filtering. The choice of method and degree to which suppression must be carried is a function of the design, materials, and workmanship used in an airplane and its electric as well as communication equipment.

Innumerable noise problems crop up in such a complex structure, and aside from atmospheric discharges these usually fall into one or more of the following fundamental classifications resulting in unwanted coupling between noisy and quiet circuits:

Capacitive Coupling. Electrostatic capacitance between noisy and quiet circuits permits radio-frequency currents to flow in the quiet circuit. This type of unwanted coupling may be reduced by more and better shielding.

Inductive Coupling. Electromagnetic flux in one current path may induce current in any loop, and therefore will usually induce voltage in any other adjacent electric circuit. Electromagnetic shielding is difficult at low frequencies but becomes somewhat easier to apply at the higher frequencies. Isolating incompatible circuits may be an effective (but perhaps in many cases unpractical) method of handling this problem.

Conductive Coupling. Where radio frequencies find a common path in noisy and quiet circuits, as frequently happens in ground circuits where the structural members are used, a simple IZ drop becomes a voltage source in circuits required to be noise-free. These sources are generally very difficult to locate and require a fair amount of imagination to treat.

Infinite variations of the foregoing factors in combination will be found in structures of normal complexity. Treatment requires putting the right impedance in the right place. In some cases it is necessary to strive for high radio-frequency impedance, say, in an unwanted induced-current loop; and in other places the objective may be a low or almost zero impedance to function as a radio-frequency short circuit. In particularly bad cases both elements may be required simultaneously to decouple portions of circuits and provide low-impedance paths simultaneously as in a radio-noise filter.

While ideally a designer should be able to predict accurately the behavior of his product, it is not surprising that in the present state of the art, arbitrary considerations govern the acceptance of many designs even though complete radio-frequency characteristics of most of the circuit elements are known. It is the unknown factors that furnish the real problems. Many times these unknown factors turn out to be simple transmission-line phenomena. Standard designs of filters[1] as well as through-stud radio-noise-suppression capacitors have been developed to meet definite needs for aircraft.

The discussion of calibrated-receiver versus noise-meter technique has always provoked much discussion. The noise meter has been accepted as a satisfactory standard for many years. Because of its unavailability up to recent times and because of the greater availability (and after all the final threshold requirements) of the receivers, a natural direct technique using only the receiver was evolved. Much could be accomplished with a signal generator as standard and an output-meter-equipped receiver of reasonable sensitivity to act as a detector that can be calibrated suitably. We find, however, that three types of error enter into measurements made with either the standard noise meter (the writer's experience has been mainly with the Ferris 32A) or with calibrated receivers. These errors (or incompatibilities) may be described as follows:

1. *Frequency.* The microvolt sensitivity of the usual circuits changes with frequency. This is taken care of in noise meters by employing a calibration which is a function of frequency.

2. *Amplitude.* At different frequencies the scale form of the output meter may vary somewhat, because of the characteristics of the associated circuits, so that, while one noise level may be correct and calibrated for, other values of voltage may be considerably in error.

3. *Wave Form.* The response of a noise meter (or receiver) may be much different on steep front than on sine waves.

In general, the authors and the writer probably agree that particularly on conducted noise tests the availability of a good standard signal generator is reassuring and is basic for any technique employed in radio-noise problems in the present state of the art, even though other than sinusoidal calibration might be desirable. The same situation seems to exist in regard to radiated tests, since the authors' Figure 6 indicates that some differences may be expected between results obtained using the two calibration means.

REFERENCE

1. RADIO-NOISE FILTERS APPLIED TO AIRCRAFT, C. W. Frick, S. W. Zimmerman. AIEE TRANSACTIONS, volume 62, 1943, September section, page 950.

E. L. Newell (Western Union Telegraph Company, New York, N. Y.): It is interesting to note that in the reduction of interference from telegraph equipment to radio receivers the Western Union Telegraph Company uses a compromise system almost identical with that mentioned in the paper. Radio-noise voltages at each source are limited to about 1,000 microvolts by suitable spark killers and filters. Additional filters are inserted in the wiring as required to prevent the residual noise from reaching the receivers either by direct propagation along the wires or by radiation from the wires.

Design of an Ignition System for an 18-Cylinder Aircraft Engine

Discussion of paper 44-189 by J. R. Harkness, presented at the AIEE Los Angeles technical meeting, Los Angeles, Calif., August 29–September 1, 1944, and published in AIEE TRANSACTIONS, 1944, pages 1321–7.

Alexander C. Wall (nonmember; P. R. Mallory and Company, Inc., Indianapolis, Ind.): It is very encouraging to see a paper on the subject of ignition-system design. There appears to have been a great lack of technical publication on the vital and complicated subject of magneto design, especially for the severe duty application to aircraft. It is a rather well-established fact that the primary factor in causing engine failure and expense in the servicing of airplanes in general is due by a very wide margin to the ignition system. It certainly seems that this subject, therefore, should receive considerable attention from a technical-design-improvement standpoint.

Mr. Harkness' paper has presented very excellently, I believe, a brief summary of a rather complicated problem.

To our knowledge, Mr. Harkness' tabulation of the various energy requirements which must be met in the design of the magneto magnetic circuit represents the first time that this information has been published, and it summarizes these requirements very well. Mr. Harkness states quite correctly that the external magnetomotive force of the magnet depends on the minimum flux density to which the magnet is ever subjected and that in this magneto this occurs when the magnet cell is removed from the magneto. This is probably due to a fortuitous combination of leakage paths which shield the magnet from the direct and opposing magnetomotive force of the primary-coil current. In many magneto designs this is a larger force than maximum air-gap reluctance. As a matter of fact, if a magneto with a freshly magnetized magnet is assembled and rotated rapidly, since the magnet has not received its primary-current stabilization and consequently has higher flux, the first flux reversal may be considerably larger than succeeding reversals and may thus knock the magnet down further than would be necessary were gradual stabilization used. Depending upon the relation of leakage reluctances, it may be desirable to use a magnet having a larger length to cross-sectional-area ratio than the magnets shown in the magneto in Mr. Harkness' paper.

Field Determination of Current-Transformer Errors by the Secondary-Voltage Method

Discussion and authors' closure of paper 44-214 by E. C. Goodale and J. I. Holbeck, presented at the AIEE Los Angeles technical meeting, Los Angeles, Calif., August 29–September 1, 1944, and published in AIEE TRANSACTIONS, 1944, December section, pages 879–82.

Otto A. Knopp (Pacific Gas and Electric Company, San Francisco, Calif.): The authors of this paper have shown that they possess a clear understanding of the physical phenomena involved in the operation of a bushing-type current transformer, and they have developed quite an ingenious method for determining in the field the ratio and phase-angle errors of such transformers.

The title of the paper does not limit the scope of the article to bushing-type current transformers nor does the text. The last paragraph of the conclusions states that the authors want to apply their method "primarily to determine the phase-angle errors and ratio-correction factors of installed current transformers for metering purposes." This, however, is not possible with the authors' method in the case of the average form-wound current transformer because of the following:

1. With the authors' method it is necessary to know the actual number of secondary turns, and this number is not disclosed by the manufacturer and cannot be calculated because of the variety of fractional-turn compensations used by the manufacturers.

2. The appreciable leakage flux in these form-wound metering transformers is another unknown factor affecting the test results when the authors' method is used.

3. Current leakage may occur between secondary turns because of a short circuit or a partial short circuit between secondary turns.

If the authors' method were limited to bushing-type current transformers, two of these objections could be disregarded. Number 2 would be eliminated entirely, as the leakage flux is extremely small and usually is neglected even for precision results. Number 1 also would not be so serious as otherwise, since with modern multirange bushing-type current transformers turn compensation is not used. Nevertheless, there may be older-type single-range bushing transformers which have been compensated, and there is also the possibility that a turn error may have occurred in the winding of old or new transformers. It appears desirable, therefore, that an electrical turn-counting method be used to augment the authors' test method.

Although the authors' method has limitations, being applicable primarily to bushing-type current transformers, it is a valuable contribution in the effort to produce a field test method. The writer would like to see this work continued and efforts made to simplify the present instrumentation. It is hoped that this brief discussion of some of the practical factors to be considered will be found helpful.

A. C. Schwager (Pacific Electric Manufacturing Corporation, San Francisco, Calif.): In the past few years, the subject of calculation of current-transformer errors has been given considerable attention. Very little, however, has been done toward improving test methods for determining the performance of actual transformers, particularly those already in service. Mr. Goodale and Mr. Holbeck have developed a method, ideally suited for field testing, which promises to find a wide application.

Whereas in the past it has been considered necessary to energize the primary of a transformer and to determine ratio and phase-angle errors by means of standard transformers, the authors have shown that these errors can be measured accurately by energizing the secondary winding and without the use of standard transformers. The method has the added advantage that the errors can be determined even at overloads without requiring too large a power supply. In the authors' case, a 120-volt source is sufficient. If the same load range would have to be covered by energizing the primary, a 220-volt source very likely would be inadequate. It appears that for determining the transformer performance at highest overcurrents, the method described constitutes the only practical solution.

It would be interesting to know if the authors have made a field test on transformers energizing the primary and, if so, what difficulties were encountered.

In plotting the test data, the authors make use of the components of the admittance vector of the secondary winding from which the errors can be most readily determined.

Due to its simplicity, this field method of determining current-transformer errors is likely to become generally accepted, and the authors are to be congratulated for this important contribution to the art.

C. A. Woods, Jr. (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): A rather novel method of measuring the magnetizing and loss components of current-transformer exciting currents is presented by the authors of this paper. As indicated, the quantities measured and their use in the calculation of ratio-correction factors and phase angles of bushing-type current transformers have been covered in several previous investigations of overcurrent performance in connection with protective relaying applications. Since the purpose of the test methods described is to make field measurements for use in determination of the metering accuracy of the transformer, any refinements in the test procedure which improve the accuracy of the results are very desirable.

It would have been of considerable additional interest, if the accuracy of the results obtained by this method were compared to those obtained on the same unit by one of the conventional methods of measurement with primary and secondary currents flowing, such as the Silsbee set method. However, the errors for the example given for the 100:5 ratio of the 115-kv bushing transformer energizing the 0.8-ohm secondary burden exceeds the range of the commercial Silsbee set. A ratio-correction factor of 1.1185 and a phase angle of plus four degrees 31$^{1}/_{2}$ minutes is to be expected of an ordinary uncompensated bushing transformer under the conditions outlined, but such a transformer normally is not considered suitable for metering service. Likewise, with such large errors and the test method used the number of significant figures given in the aforementioned calculated results hardly appear to be justified, since the accuracy of commercial "nul" methods of testing is on the order of ±0.1 per cent in ratio and ±6 minutes in phase angle.

The authors well might have indicated the need for some precaution relative to the wave shape of the voltage source used for the excitation tests. Preferably the excitation source should be a good sine wave, but, if voltmeter number 2 is a high-resistance rectox instrument (reading average values in rms terms), reasonably good results will be obtained in exciting current measurements even with considerable distortion in the excitation voltage wave. Emphasis should be placed also on the fact that the method proposed is based on the assumption that the number of secondary turns dictated by the transformer design are known and that they are the actual turns on the transformer under test.

A very desirable testing method for obtaining the magnetizing and loss components of current-transformer secondary exciting currents with open-circuited primary has been given. It should prove useful not only for field testing but also for laboratory testing. Its accuracy is dependent upon the accuracy and calibrations of the test instruments and the care used in determination of the test circuit constants and taking of readings. There is ample justification for neglecting the equivalent secondary leakage reactance of bushing current transformers and using the excitation characteristics to determine performance for relaying applications. For such applications the determination of excitation characteristics with ammeter, voltmeter, and wattmeter measurements with corrections for losses in the transformer secondary winding and the instruments is usually sufficiently accurate. However, for metering applications the open-circuit excitation method may be of questionable accuracy. Everything considered, it would appear quite possible that the over-all method would not be sufficiently accurate for checking ratio-correction factors and phase angles of cuerrnt transformers having metering accuracy classifications in accordance with American Standard Association "Standards for Instrument Transformers." For such purposes one of the "nul" methods involving measurements with primary and secondary currents flowing would be desired, unless it can be shown that the accuracy obtained with the secondary-voltage method compares favorably with that of the "nul" methods.

E. C. Goodale and J. L Holbeck: The discussions by Mr. Schwager, Mr. Knopp, and Mr. Woods are all notable contributions relative to current-transformer test methods.

In those cases where wound-type current transformers have not been available due to war conditions, it has been necessary to resort to bushing-type current transformers for metering purposes on high-voltage circuits. Then it became necessary to have accuracy curves for the bushing current transformers so used. This in turn made it desirable to develop a simple field method for determining the transformer errors.

It is not intended that this test method should be used to replace the Silsbee test set but rather to supplement it, especially in those cases where the transformer ratio errors are outside the range of application of the Silsbee set. For relay application, errors at high overloads can be determined from an ordinary 120-volt test source by the secondary-voltage method. However, the limitations of this method are recognized as this method has its definite field of application, namely:

1. Bushing current transformers of known turn ratio with special emphasis on multiratio transformers.
2. Wound-type current transformers of known turn ratio which have not been compensated.

The method was used further in calibrating bushing-type current transformers for use with station metering. Obviously the use of uncompensated low-ratio bushing-type current transformers for billing meters would not be permissible from an ASA accuracy standpoint, as the error curves would not be flat enough to allow for their compensation under varying loads by meter-magnet and lag-coil adjustment only.

In the case of relay applications the method is particularly useful when there is a large phase shift as is true on installations having long secondary leads.

It is true that accuracy tolerances of less than 0.1 per cent in ratio and less than four minutes in phase angle are not easily obtainable by this or any of the present methods of testing. Final values in the example described in the paper were carried out to four decimal places, whereas three would be sufficient. It may be stated that the practical limit of accuracy, using field-testing instruments of one per cent accuracy limits should be within 0.2 per cent in ratio and seven minutes in phase angle. However, by using a regulated voltage of pure sine wave, instruments having accuracy limits within one-fourth per cent, and with exact measurements of the test burdens a finer degree of accuracy may be expected.

The diagram of test equipment and connections in Figure 1 may be simplified by replacing the wattmeter with a combination single-phase wattmeter–varmeter and a suitable watt–var selector switch. Then the potential-type phase-angle meter may be eliminated.

A further simplification, which does, however, sacrifice a certain amount of accuracy, can be accomplished by eliminating the phase shifter, compensating burden, 50–50-volt transformer and phase-angle detector. This may be done by applying the test voltage at the secondary terminals of the current transformer. Under this condition the voltage E_a, applied to the wattmeter current coil and current-transformer secondary winding in series is slightly higher than the back electromotive force or voltage, E_s, induced in the secondary winding. This difference may be neglected without too serious an error, estimated within two per cent, if the voltage E_a or E_s is kept within ten volts, when testing a bushing current transformer on the 200–5 ampere ratio. The impedance of the wattmeter current coil should be kept as low as possible without sacrificing reading accuracy. One method is to use two wattmeters, a low range (relatively high impedance) meter for low values of excitation, and a five- or ten-ampere (low-impedance) instrument for the higher values. In this way the impedance drop caused by passing test current through the wattmeter current coil is kept to a minimum, resulting in greater accuracy.

These approximate methods are stressed here, as it is believed that in a great many cases the equipment will not be available for the complete tests, and much useful data which otherwise would not be available may be obtained in this way.

Tests of 230-Kv High-Speed Reclosing Oil Circuit Breaker

Discussion and author's closure of paper 44-192 by A. C. Schwager, presented at the AIEE Los Angeles technical meeting, Los Angeles, Calif., August 29–September 1, 1944, and published in AIEE TRANSACTIONS, 1944, November section, pages 784–8.

R. A. Larner (Texas Electric Service Company, Fort Worth, Texas): It is significant to note that very good reclosing performance at 230 kv has been obtained using ordinary expulsion contacts similar to those tested previously on lower voltages. After several years' experience, the writer was convinced that this contact was good, but it had not been evident that the performance would be quite so good at higher voltages as data presented by Mr. Schwager show quite conclusively.

It has been suggested previously that it would be well to lessen the time of circuit-breaker closing or reclosing to a value that would be commensurate with recent developments of three to five cycle opening time. Data indicate that the breaker described has sufficiently short time to meet any ordinary requirements of the industry at present.

Considerable data are presented showing effects of different opening speeds in feet per second. These speeds seem relatively high in comparison to a great many tests of older breakers, made on a Texas system, using a time-travel instrument. This perhaps indicates a necessary trend toward higher-contact travel speeds which in turn likely will mean the use of better grades of steel along with lighter metals or materials in the operator and connecting linkage of oil circuit breakers. In this particular breaker some very interesting linkage is used in the operating mechanism. It appears, also, that the now well-known bevel link is used between the horizontal and vertical shafts. If the writer may be permitted to make a suggestion, it would be that papers on oil-circuit-breaker tests should include complete time-travel diagrams showing tripping and reclosing characteristics.

T. M. Blakeslee (Department of Water and Power, Los Angeles, Calif.): This discussion deals primarily with the subject of charging-current interruption, as the system of the Department of Water and Power does not include single-pole-reclosing breakers. The idea of single-pole reclosing is, however, attracting considerable attention, and the author is to be complimented on the effective demonstration he has made of a high-voltage high-speed device for this application.

In connection with the interruption of charging currents, which Mr. Schwager points out is the severest duty that can be imposed upon circuit breakers, the point made in the paper regarding the desirability of a very low dielectric-recovery rate is an interesting observation. In other words if one, in trying to build a breaker that will not restrike, does not fully succeed, he may be worse off than if he never made the effort. For if he falls short of his goal, the result may be delayed restrikes which, as Mr. Schwager indicates, are the type of restrikes that produce harmful voltages.

The author concludes that resistors across the breaker contacts produce no improvement in performance on the interruption of charging current. Recent tests made by the Department of Water and Power on the interruption of 138-kv cable charging currents have indicated a considerable reduction in overvoltages by the use of resistors. The resistors do not eliminate restrikes, although they tend to do so, but, because the gap voltage is reduced, the resulting overvoltages due to restrikes are materially reduced.

In this connection, it is noted that Mr. Schwager reports more arcing with resistors than without, and I should like to ask him to elaborate on this point. With resistors in the breaker there are two sets of contacts to part, the main contacts and the contacts which finally disconnect the circuit, and arcing can occur on each set of these contacts. Also, what are the dead-time characteristics of the breaker with the resistors installed; that is, how long are the resistors in the circuit on the opening stroke?

The Department of Water and Power has had this problem of charging-current interruption for some time. A 90-mile section of the 287.5-kv Boulder Dam transmission line requires 34,000 kva for energization at full voltage. The system now has three 138-kv underground-cable circuits, two of which require 19,000 kva each, and the other 9,000 kva for energization. Many tests have been made on switching these circuits, and one of the significant and impressive observations on the performance of breakers working on the principle of self-extinction of the arc is what some writers refer to as "the randomness of the restriking phenomena." This makes testing on actual systems somewhat of a problem, as the true picture is not likely to be obtained unless a very large number of tests are made.

For example, out of 21 line-dropping tests at Boulder Dam, only one resulted in sufficient overvoltage to cause discharge through the line lightning arrester. Out of some 65 openings on a 138-kv cable circuit, only six resulted in sufficient voltage (about three times normal) to cause discharge through lightning arresters on the cable circuit; one of these six operations resulted in failure of an arrester.

W. F. Skeats and **E. B. Rietz** (General Electric Company, Philadelphia, Pa.): Though it is not quite clear from the oscillograms just how the tripping time is determined, Mr. Schwager appears to have developed a 230-kv interrupter with a safe margin under five cycles interrupting time at duties of the order of 750,000 kva. It is reasoned that, since the effect of increase in current is "so well established" by earlier tests at 110 kv, it may be assumed safely that the interrupting time at the full rating of 2,500,000 kva at 230 kv will be no greater than in the tests reported in the present paper. However, a review of the references to the older work indicates that, while in one arrangement using six breaks at 110 kv there was a reduction in arcing time of one tenth of a cycle with increase in current from 130,000 kva to 550,000 kva, tests with three breaks at 110 kv show an increase in arcing time of almost a full half-cycle for a similar current increase, and tests on six breaks at 230 kv, corresponding to the present arrangement, show a similar increase for a change from 500,000 kva to 1,100,000 kva.

It appears that the decreased interrupting time reported in the present tests is attributable to very high mechanical speeds. We are assuming that tests not referred to in the paper prove that the operating mechanism will give the same opening characteristics for a three-pole breaker. The rapid extension of the arc associated with such speeds often results in severe chamber pressures. Apparently these cause no damage at the relatively low currents in the present tests, but we question what assurance there is that such pressures will not become excessive at the full interrupting rating of the breaker, particularly in view of the increased arc durations which earlier tests appear to indicate.

In the second operation of the reclosing test for which the oscillogram is shown in Figure 5, Mr. Schwager states that the arcing time was only one-half cycle. Yet for $2^1/_2$ cycles before clearing there is a disturbance in the record of voltage across the contacts. Could Mr. Schwager tell us just how this disturbance arose?

On charging-current interruptions, the performance seems satisfactory. However, it should be pointed out that the voltages of $4^1/_2$ times normal which Mr. Schwager refers to as having been experienced with other interrupters did not occur on a circuit with the neutral solidly grounded. Under this condition reference 6 of the paper shows the voltage on either side of the breaker to have been limited to the very reasonable value of 2.3 times normal.

A stored-energy type of operating mechanism seems to be required for such high-speed reclosing of high-voltage breakers. Factory tests with a standard pneumatic operating mechanism such as used for 115- to 230-kv breakers indicate that 20-cycle reclosing is practical on modern 230-kv breakers with reasonable modifications of the moving-contact parts to reduce their weight.

W. M. Leeds (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The observations made in this paper on the interruption of line-charging current are of considerable interest, confirming theoretical predictions that repeated restrikes with current-zero intervals of less than one-quarter cycle produce no overvoltage. The possibility of overvoltage building up cumulatively to hazardous values apparently is avoided by a circuit breaker which operates with not more than

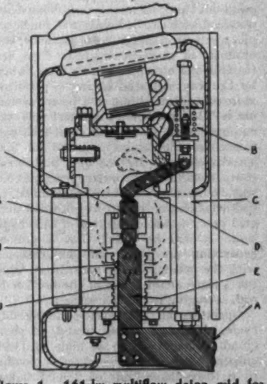

Figure 1. 161-kv multiflow deion grid for 3,500,000 kva

Tripping of breaker releases main crossarm *A*, allowing springs *B* to move operating rod *C* down and rotate contact arm *D*, drawing pressure-generating arc. Simultaneously, contact *E* pulls away from intermediate contact *F*, drawing main arc. Pressure from upper chamber drives oil through channels *G* to multiflow inlets *H*, deionizing arc in orifices *I*, and sending arc products out vents *J*

Figure 2. 161-kv multiflow grids

one restrike having a current-zero interval exceeding one-quarter cycle.

The author states that an amount of oil sufficient for multiple interruptions is provided in the expulsion chamber, indicating that all of the gases may not have time to escape before reclosure. Residual gases not only displace oil needed for effective arc rupture but invite excessive prestriking of the arc during the closing operation. It is not clear why adequate interrupting ability on normal-duty cycles should be considered sufficient assurance of satisfactory performance on high-speed reclosing, unless actual tests are made at the higher short-circuit currents where the gas generation is a maximum.

The use of multibreak contacts operated at very high mechanical speeds has been shown to be effective in cutting down the arcing time to the point where breaker interrupting times of five or even three cycles are possible. However, it may be of interest to point out that comparable results have been obtained with circuit breakers of much simpler mechanical design. For instance, large numbers of short-circuit tests have been made in the high-power laboratory (where, by the way, repeated testing is performed more conveniently than on a power system) on a 72-inch-diameter-breaker pole unit equipped with a standard eight-inch compressed-air mechanism adjusted for high-speed reclosing. Instead of a multibreak-contact assembly, one interrupting unit only was mounted on each breaker terminal, the contacts being of the 161-kv multiflow deion grid type, shown in Figure 1 of this discussion. It will be noted that a pressure generating arc is drawn simultaneously with the opening of the main break which takes place at the speed of the breaker lift rod, averaging 13 feet per second.

Operating on a 20-cycle-reclosing duty cycle, this breaker was tested on short-circuit currents up to as high as 11,000 amperes at 88 kv to ground, corresponding to a three-phase fault of 2,900,000 kva. The oscillogram of Figure 2 of this discussion for this highest power test shows that the two interruptions took place promptly with only 1.5 and 1.7 cycles of arcing, respectively, and with very low arc-energy dissipation.

It has been demonstrated, therefore, that circuit breakers equipped with only two interrupters per pole and operating at reasonable speeds, when properly designed, can perform the functions of high-speed interruption and fast reclosure without the mechanical complication or high-contact velocities of the multibreak device described in this paper.

E. W. Boehne (General Electric Company, Philadelphia, Pa.): Mr. Schwager has presented some very interesting tests and conclusions which will bear a little further consideration, particularly with respect to the features which bring about the successful interruption of line-charging kilovolt-amperes. It is an established fact that the higher the speed of contact separation, the less is the possibility of restriking during line-charging interruptions. The same breaker, however, is required to interrupt light inductive-load currents as well as the small magnetizing current of associated transformers. Here an excessive contact speed encourages the "off-current-zero" interruption of the small inductive current. It has been shown[1] when an interrupter produces a shear "off-current-zero" interruption of a purely inductive current, at an interval prior to the normal current zero equal to the period of the natural frequency of the circuit, that a voltage surge of 7.25 times normal voltage crest is produced on the inductive equipment. Excessive contact speed encourages this characteristic which is augmented further in the presence of circuits having high natural frequency, such as occur when the breaker is adjacent to a transformer. Hence, contact speed usually cannot be dictated by the capacitive kilovolt-ampere requirements alone but must be based upon the entire spectrum of currents both leading and lagging. It would complete the picture if Mr. Schwager supplemented his paper with such low-current tests at both leading and lagging power factors.

With this fact recognized, and the requirements of interrupting high line-charging currents known, it was found expedient (reference 6 of subject paper) to retain the features which achieved the safe interruption of the low inductive currents by not increasing the contact speed but instead by supplementing the breaker interrupting contacts with ohmic resistors. An analysis, confirmed by test, showed that the over-all optimum conditions were created by adding a total resistance per pole equal to 2.11 times the capacitive ohms to neutral. This value, although optimum, is not critical, and resistors approximating this value on either side will be found beneficial. Resistors in this range held all overvoltages to an exceedingly safe level (reference 6) on a system which was not solidly grounded. Breakers on grounded systems, if they require resistors at all, could permit values of resistance considerably higher than this value.

This philosophy applied to the circuit described by Table II of Mr. Schwager's paper, indicates a value of resistance per pole of $130{,}000/105 \times 2.11 = 2{,}600$ ohms, drawing a current of only 45 amperes per phase. The 3,000 ohms per pole (37.5 amperes) used by Mr. Schwager is in good agreement with these values (reference 6). It is emphasized in the subject paper, however, that the arcing time for these trivial resistor currents increases the over-all arcing time, for line-dropping tests, by 1.2 cycles. The significance of this fact is not clear. The conditions of line-charging-current interruption do not represent any fault condition which would hinder rapid reclosure. These small currents produce no appreciable heating, and, even should they follow the power current associated with a line flashover, they should not alter appreciably the permissible reclosing time. Mr. Schwager has shown no power tests involving the resistors; hence confirmation in this case is not possible.

Mr. Schwager indicates, with a very limited number of tests, that resistors across the interrupting elements create no improvement in performance. This is difficult to understand, because the introduction of shunt resistors, in addition to assisting the interruption of leading currents, decreases the rate of rise of recovery voltage on inductive circuits which, according to all previous standards, is an accepted method of reducing the circuit severity. An indication of this fact is the values given in Table I of the paper. Here 66 per cent of the tests shown for the higher recovery rates (test 13 and 26, first opening) showed longer arcing times, notwithstanding the fact that the current interrupted was more than double that in the tests at lower recovery rates. It would have been of value to have included tests with resistors at higher currents to permit this feature of the resistor to be demonstrated.

The conclusion is therefore drawn that resistors in interrupters, designed to perform adequately on the entire spectrum of lagging currents, are an asset to the performance of the same interrupters on large capacitive kilovolt-amperes on ungrounded systems or systems operating with ground-fault neutralizers. Resistors in breakers as previously described should prove of assistance to both capacitive and inductive interruptions and should not interfere with the modern trends in circuit-breaker development and applications.

The one single-phase-reclosing test shown in Table I hardly justifies conclusion 2, since the mechanism had only to overcome the closing forces for a single pole.

REFERENCE

1. THE GEOMETRY OF ARC INTERRUPTION—II; CURRENT-ZERO PHENOMENA. E. W. Boehne. AIEE TRANSACTIONS, volume 63, 1944, pages 375–87.

A. C. Schwager: The paper described charging-current-interruption tests with and without parallel resistances across the arc-rupturing devices. It was shown that the arcing time is longer with resistances than without resistances, and several of the contributors to the discussion state that such performance is contrary to that experienced with other rupturing devices. As pointed out by Boehne, Skeats, Rietz, and Leeds, the short arcing times during interruptions without resistances are due to the relatively high blade speed used. At a breaker speed of 24 feet per second the average arcing time amounts to 1.2 cycles.

In answer to Mr. Blakeslee's question, when resistances were used at 24-feet-per-second speed they remained in the circuit for approximately 1.5 cycles. In other words, without the use of resistances the charging current is interrupted before the instant that the final arc would be drawn in case resistances were used. With resistances, approximately 1.5 cycles of arc is present after the blade leaves the auxiliary contacts.

As Mr. Boehne points out, an over-all arcing time of three cycles for line-dropping tests is not excessive; however, if this same line can be dropped without resistances across the contacts in less than three cycles (actually 1.2 cycles), then it is obvious that the resistances would not constitute an improvement. It is not denied however that resistances are useful in conjunction with other rupturing devices, which without resistances might have excessively long arcing times and numerous restrikes and which would produce overvoltages. With the advances in high-strength lightweight metals, the attainment of high speed presents no unusual problem; its use leaves the expulsion chamber in its original simplicity and eliminates the necessity of resistances or additional moving parts and contacts. Mr. Boehne further points out that high breaker speeds are contraindicated for the interruption of small inductive currents. Actual field installations in conjunction with 5,000-kv reactors, however, demonstrate that the expulsion-chamber-breaker interruption proceeds entirely normally, that is, without excessive tank pressures or oil throw. Mr. Boehne states that resistances should be helpful in reducing the arcing times during heavy current interruptions, because of the smaller rate of rise of recovery voltage produced through them. It is possible that the use of resistances would reduce the arcing times in tests 13 and 26 from 1.4 to possibly 1.0 cycles. In conjunction with five-cycle breakers such a reduction is not necessary. For three-cycle breakers, it is felt that other methods of reducing further the arcing time should be considered before using the hazardous resistances. Conclusion 2 referred to the ability of the expulsion chamber to interrupt successive short circuits in quick succession. This performance is of practical importance in conjunction with single-pole-reclosing operation of transmission lines.

Skeats and Rietz bring out the fact that the sloping arcing time–current characteristic of the expulsion chamber might not be sufficiently established to permit extrapolations as to performance at full ratings. If differences due to random phenomena of approximately one-quarter cycle are disregarded, this characteristic has been established by numerous additional field tests. As an example, on an extended test on a 69-kv breaker with expulsion chamber the arcing time gradually decreased from two cycles at 212,200 kva to one cycle at a maximum of 1,327,500 kva. With reference to the voltage record of Figure 5 of the paper, this is disturbed by a high-frequency oscillation of the potential transformer employed, a fact which becomes particularly obvious by analyzing the arc-voltage record during the first interruption, which shows the lightly damped oscillation. As can be seen, the oscillation continues for several cycles after the interruption and therefore cannot represent the actual condition.

Mr. Leeds demonstrates by means of figures and oscillograms that fast-charging-current and short-circuit-current interruptions can be accomplished by the use of auxiliary contacts. As previously pointed out, it is felt that the elimination of any auxiliary devices is advantageous from a maintenance standpoint, particularly if satisfactory performance can be guaranteed by a simple expedient as higher blade speed.

Mr. Larner confirms the fact that by the liberal use of high-strength metals, bevel links, and other advanced mechanical designs, higher breaker speeds are easily obtained. Space limitations precluded the inclusion of the valuable time-travel curves in conjunction with these tests.

A Distance Relay With Adjustable Phase-Angle Discrimination

Discussion and author's closure of paper 44-216 by S. L. Goldsborough, presented at the AIEE Los Angeles technical meeting, Los Angeles, Calif., August 29–September 1, 1944, and published in AIEE TRANSACTIONS, 1944, November section, pages 835–8.

L. L. Draper (Department of Water and Power, Los Angeles, Calif.): The relay element described in this paper is a distinct forward step in the protection of long transmission lines, particularly with respect to the convenient adjustments that have been provided.

By shifting the center of the characteristic circle away from the origin in the general direction of the line-impedance vector, on the resistance–reactance diagram, it is possible to reduce greatly the diameter of the circle without affecting the element's sensitivity to faults. This reduction in diameter, however, makes the element much less sensitive to swings and loads.

If we refer to Figure 5 of the paper, there are two questions I would like to ask the author:

1. In swinging the phase angle of the element's characteristic circle away from the line-impedance angle, would it not be necessary to allow a greater safety factor in setting the cutoff point for line faults, because of the more acute angle at which the line-impedance vector intersects the characteristic circle?

2. How would the situation at the remote end of the line be handled where the load and swing vectors would approach from the left instead of from the right as shown?

It seems to me that the optimum phase-angle adjustment for this element would be the same angle as the line, since this adjustment would give equal sensitivity to loads and swings approaching from either direction and would, I believe, allow the most accurate adjustment of the cutoff point for line faults.

Although this element has directional characteristics, as stated in the paper, it is necessary to supervise its operations by a conventional directional element so as to secure positive discrimination between close in-line faults and faults on or back of the local bus.

The maximum swing angle between two parts of a system that can be attained with the system still recovering stability is a somewhat indefinite quantity. I have seen automatic oscillograms of swing conditions in which one or more cycles have been gained and lost between two parts of a system and the system regained stability. It is my opinion, therefore, that all major transmission-line protection on lines through which an unstable condition might exist should be equipped with out-of-step blocking, and, if it is felt desirable to separate automatically two parts of a system that are out of step, this should be accomplished at some definite location by relays that operate after a predetermined number of cycles has been gained and lost.

E. H. Klemmer (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Distance relays with adjustable characteristics as described by Mr. Goldsborough will find other applications besides those dictated by stability considerations between generating stations or systems. Other types of applications where such a relay might be employed to advantage, which come to mind, involve lines with tapped-off motor loads (synchronous or induction), such as pumping stations, mills, and electric railways. Here it may be difficult to discriminate with ordinary relays between remote-end line faults where tripping is desired and motor-accelerating conditions where tripping is to be avoided. Motor acceleration must be considered not only during starting conditions which usually involve only one or a few motors at a time, but more particularly for the acceleration following the clearing of an external fault when all the motors may be involved. This acceleration involves currents comparable to the combined starting current of all motors possibly five or six times full load and at quite low power factor, depending on the motor speed or slip. Hence the accelerating impedance and its angle may become comparable to a long-line remote-end fault.

A. R. van C. Warrington (General Electric Company, Philadelphia, Pa.): This paper touches upon the contents of the two original papers on the mho relay[1,2] and then shows how current bias can be introduced into the beam-type impedance relay to obtain the mho characteristic described in those papers. The author goes on to show that the characteristic not only can be pushed into the mho circle position but also can be pushed farther in all directions so as to be entirely clear of the origin. The advantage of this is not at all clear. Figure 5 purports to show that the relay can be made very insensitive to

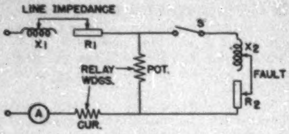

Figure 1. Circuit for testing relay on suddenly applied faults

$$\text{Set } \frac{X_1+X_2}{R_1+R_2}=5=\tan^{-1} 78°$$

Compare steady balance point of relay with highest value of ohms that will never operate relay when S is closed

power swings by pushing the characteristic over to one side of the system impedance vector. The author apparently has overlooked the fact that the swing impedance can come in from the other end of the power-swing locus when the power flow has swung to the incoming direction.

It is very difficult to understand the author's explanation of the operation of his relay, because there are apparent errors in the text and the diagrams and he does not explain what all the mathematical terms represent. A very much simpler explanation is as follows: The ampere-turn equation of the unit is $[E-K_RI \cos (\phi-\theta)]=[K_0I]$. Dividing through by I gives the impedance equation $[Z-K_R \cos (\phi-\theta)]=[K_0]$. This is the equation of a circle on an impedance diagram whose radius is K_0 and whose center is K_R from the origin at an angle θ from the reference vector. K_0 corresponds to the adjustment of the current operating coil I_0, the Z_0 setting; K_R corresponds to the Z_R setting; θ is controlled by the resistor R_ϕ.

The characteristic of the original balanced-beam impedance unit was not exactly circular, and it is hard to see how the additional biasing means could provide any such regular circles as those shown in the paper. Does the author have any test results corresponding to the circles shown in Figures 8, 9 and 10 of the paper?

In an ordinary impedance relay offset currents, such as may be obtained at the inception of a fault on a transmission-line circuit of $X>3R$, can cause considerable overreaching, that is, liability of the relay to trip on faults beyond the setting of the relay. In the new relay the overreaching would appear to be still worse, because the increased values of current associated with the offset would reduce the restraint as well as increase the operating torque. Has the author made any tests with faults suddenly applied in a highly lagging circuit? The illustration shows a simple test circuit which represents the appearance of a fault on a transmission line and can be used to check the behavior of the relay.

References

1. Protective Relaying for Long Transmission Lines, A. R. van C. Warrington. AIEE Transactions, volume 62, 1943, June section, pages 261–8.

2. The MHO-Carrier Relaying Scheme, R. E. Cordray, A. R. van C. Warrington. AIEE Transactions, volume 63, 1944, May section, pages 228–35.

S. L. Goldsborough: In answer to Mr. Draper's question regarding the setting of the cutoff point when the axis of the characteristic circle is shifted away from the line-impedance angle, the safety factor, figured on the basis of the intersection of the circle with the line-impedance vector, would have to be greater. However, if the safety factor were figured on the minimum distance of the line-impedance cutoff point to the circle, it would not have to be greater.

Mr. Draper's second question brings up the question of the response of the relay to synchronizing surge vectors which approach from the left instead of from the right. An inspection of Figure 5 of the paper will reveal that relay operation is prevented by the nonoperation of the directional element whose zero torque line is shown leading the JX axis by 60 degrees. If the surge vector on the left should lead the JX axis by an angle not smaller than the angle of lag of the right-hand surge vector shown, adequate cutoff would be provided by the directional element. However, if the angle of lead of a surge vector on the left were considerably less, it would, of course, fall on the operating side of the directional-element zero torque line as shown, but this situation could be remedied very easily by energizing the directional element with star currents which would give a zero torque line leading the JX axis by only 30 degrees.

It is apparent from Mr. Warrington's discussion that he does not appreciate the advantage of being able to displace the characteristic circle so as to include or not to include the origin within the circle. This ability to shift the circle at will simply means that, in severe cases where the surge vectors from both the right and the left approach very close to the line ohms, the relay characteristic can be made to cover the line ohms adequately without including the surge-ohm vectors. This can be done in two different ways, by using two elements with their contacts in series. One way is to overlap two small-diameter circles with their centers on the line-impedance vector, and the other way is to overlap slightly two large-diameter circles with their centers on a line at approximately 90 degrees to the line-impedance vector and on opposite sides of the line-ohm vector.

Evidently Mr. Warrington did not understand all of the information contained in Figure 5, or he would have seen that surge ohms which approach from the left are cut off by the directional element. Because of this, it is permissible to shift the circle to the left to avoid the surge vector on the right. Of course, if the circle periphery must pass through the origin, the circle cannot be shifted to the left, since then it would not encompass the region on the R axis representing close-in arc faults. This difficulty is overcome in the relay described by shifting the circle downward to include the proper region on the R axis, as is shown in Figure 5.

Mr. Warrington, in bringing up the point that the original impedance unit did not have an exactly circular characteristic, is confused regarding the phase-angle relationship of the current force on the front end of the beam with respect to the force on the rear end of the beam. The original impedance unit was deliberately made somewhat responsive to the phase relation of the quantities on the two ends of the beam, and this caused the deviation from an exact circular characteristic. However, the impedance unit described is made substantially independent of the phase angle between the opposing quantities acting on the two ends of the beam, and, therefore, the characteristic is substantially circular. This point was brought out in the paper by the statement: "No vector relationship exists between the pull on the front end and the pull on the back end of the beam, since the beam has substantially no response to the phase angle between the forces on the two ends."

Evidently Mr. Warrington has not attempted at all to analyze the action of the circuit in Figure 6 on d-c transients but has jumped to the conclusion, apparently engendered by wishful thinking, that response of the element would be excessive because of the introduction of current on the rear of the beam in opposition to the voltage. Mr. Warrington has overlooked two very important points. In the first place the phase-shifting elements, the reactor X_0 and the resistor R_t, constitute a transient shunt. The high-angle reactor X_0 has a low d-c resistance and, therefore, the d-c transient is shunted through the reactor and does not show up in the resistor R_t. Another more important effect is that during the transient period the current in the resistor R_t does not have its proper phase relation. The function of the X_0, R_t circuit is to lag the current in R_t 60 degrees from the current I_R. It requires time for any phase-shift circuit to get going, and, therefore, during the transient period the current in R_t is not lagged the proper amount. This means that during this period the current on the back end of the beam is not subtracting directly from the voltage, and, therefore, its combination with the voltage gives a resultant which is larger than it should be until the d-c transient disappears. The excessive restraint then tends to balance the excessive current on the front end of the beam since the d-c transient is not removed from this coil.

Actual tests have shown that the response of the element to d-c transients is negligible. On a 78-degree circuit the balance point shifts only four per cent on the maximum possible transient.

D-C Arc Interruption for Aircraft

Discussion and authors' closure of paper 44-186 by J. S. Quill and L. T. Rader, presented at the AIEE Los Angeles technical meeting, Los Angeles, Calif., August 29–September 1, 1944, and published in AIEE TRANSACTIONS, 1944, December section, pages 883–8.

Cyril G. Veinott (Westinghouse Electric and Manufacturing Company, Lima, Ohio): Quill and Rader have presented some valuable experimental information which should be of appreciable assistance not only to builders of switches but also to users of them. While there is no apparent reason to doubt the accuracy of their data on arc interruption and no apparent reason to challenge their general qualitative conclusions, there is reason to discuss their method of reproducing altitude conditions. They state that they have reproduced the standard National Advisory Committee for Aeronautics pressure at altitude but not the temperature. Arc-interruption phenomena are a function of density of the air, not pressure alone; hence, the standard NACA air density at altitude should have been reproduced instead of merely pressure. Since temperature is not of itself important, the authors were justified in not taking the additional trouble to reproduce the standard NACA temperatures, but their results should have been plotted as a function of density altitude, not pressure altitude.

To illustrate the difference—if the pressure altitude is 50,000 feet, and if the temperature of the air is +25 degrees centigrade, the density altitude is 56,600 feet. Thus, the data given by the authors for 50,000 feet really apply for an altitude of approximately 57,000 feet. But it must also be remembered that the density altitude of the switches in an airplane may differ appreciably from the pressure altitude, particularly if the switches are in a warm ambient temperature; and it is the density altitude which governs the performance of the switches.

J. S. Quill and L. T. Rader: Mr. Veinott has correctly stated that a temperature correction factor should be applied to the conversion of pressure to altitude. However, it is obvious that this temperature correction is merely a matter of changing the altitude co-ordinate and in no way affects the accuracy or usefulness of the results. It merely makes them conservative. The introduction to this paper states that this investigation was carried out to obtain *preliminary* data on arc-interruption problems introduced by the use of higher direct voltages. When this work is done in its advanced stages, we believe that it will be necessary to duplicate actual temperatures as well as pressures. We do not subscribe to Mr. Veinott's statement that temperature is not of itself important but ultimately will test under as near actual conditions as possible. Air density has a part to play in arc interruption, but the temperature of the electrodes, especially if the gaps are very small, is also important.

Low temperatures were not used in this investigation, because we felt they would help arc interruption, and we therefore obtained data with some inherent safety factor and made testing much more rapid. The comparative data presented are still valid and useful.

Requirements for Low-Voltage Aircraft Cable

Discussion of paper 44-224 by R. E. Hedges, presented at the AIEE Los Angeles technical meeting, Los Angeles, Calif., August 29–September 1, 1944, and published in AIEE TRANSACTIONS, 1944, November section, pages 808–10.

Joseph H. Jay (nonmember; Air Technical Service Command, Wright Field, Dayton, Ohio): The use of any suitable material that meets the performance requirements of the applicable low-voltage aircraft-cable specification is the driving factor for the cable manufacturers. R. E. Hedges brings forth this point in his summary and also covers the majority of the requirements as outlined in specification *AN-J-C*-48. However, it should be stressed that the use of any suitable material has been an incentive to the cable manufacturers and has given them a wide field to draw from to meet aircraft requirements.

It should be noted that the manufacturers of aircraft cable have used a wide assortment of insulating materials in meeting aircraft requirements. The majority of materials used seemed to indicate that the ingenuity of the manufacturers has been put into excellent use, as very few repetitions of identical insulating materials have been encountered in a wide field of manufacturers.

The splendid co-operation of the wire manufacturers has resulted in a very satisfactory aircraft cable, though new requirements are continually encountered, and the present requirements as covered by the *AN* specification have been capable of withstanding these new environments. To insure a satisfactory wire for conditions which have not been previously encountered has created the changes in requirements and appropriate tests are changed or new tests inserted in the specification.

Electric Connections on Aircraft

Discussion and authors' closure of paper 44-222 by F. O. Stebbins and L. A. Taylor, presented at the AIEE Los Angeles technical meeting, Los Angeles, Calif., August 29–September 1, 1944, and published in AIEE TRANSACTIONS, 1944, December section, pages 906–11.

Jerome H. Ehrich (nonmember; lieutenant, Air Technical Service Command, Wright Field, Dayton, Ohio): This paper presents an interesting survey of electric connections in common use in aircraft. In regard to stripping of wire, a table is included, showing allowable broken strands per wire and a maximum allowable percentage of scraped, swaged or nicked strands. The insulation-stripping operation should be accomplished without damaging or breaking any of the wire strands. Setting up of standards for permissible damage to wire will result in careless wire stripping. An employee who knows that a certain number of strands may be severed or damaged in the stripping operation may make no special effort to strip a wire without damage to the wire strands.

A wire stripper which will strip aircraft low-voltage cable fabricated in accordance with specification *AN-J-C-48a* without damage to the wire strands is currently in the process of standardization.

With reference to *AN* connectors, the most successful *AN* connector contact tested to date consists of a tubular member machined from high-conductivity-copper bar stock. The contact is divided into two separated cavities. One cavity, the solder pot, receives the wire lead. The other is the pin cavity, the wall of which is cut into several segments, each flexible enough to make individual contact with the pin by the pressure applied on the forward end of the contact by a circumferential spring. This construction allows for a low potential drop, since there is one line of contact per segment on the pin.

After the standardization of *AN* connectors was accomplished, an effort was made by aircraft manufacturers to use connectors for all possible applications requiring frequent disconnection. Connector-insert arrangements multiplied until at present over 200 insert arrangements are available.

Experience with *AN* connectors during the past four years proves that with proper initial wiring of *AN* connectors and installation used in aircraft, in locations not subject to extreme vibration, heat, or splashing rain and liquids, *AN* connectors have been giving satisfactory service.

To date, no phenolic has been developed for connector inserts which would enable them to withstand the terrific vibration encountered on equipment mounted on the aircraft engine. For these applications, lugs and studs are being used.

Although *AN* connectors are constantly being improved by use of superior materials and contacts, the trend at present is towards the use of *AN* connectors only where absolutely necessary. Where heavy currents are encountered, lugs and studs are being specified and, wherever applicable, terminal strips are being utilized for connecting electric circuits. However, there is still a definite need for *AN* connectors since they do operate with maximum efficiency when the following conditions are met:

(*a*). Protection should be assured from extreme vibrations and high temperatures. (No connectors on engine-mounted equipment nor in poorly ventilated locations in the airplane-engine section.)

(*b*). *AN* connectors should be sparingly used for circuits carrying more than 50 amperes.

(*c*). *AN* connectors should be installed in locations where they will not be subjected to dripping oil, gas, hydraulic fluid, or rain water.

E. C. Quackenbush (nonmember; American Phenolic Corporation, Chicago, Ill.): One of the main features of *AN* connectors is their *interchangeability*. When the specifications were written, it was the desire of the Army and Navy engineers to write these specifications in such a way that the various manufacturers would have freedom in design, yet the performance would meet a certain standard, and the products of the various manufacturers would be interchangeable. This method of writing specifications may be open to criticism, but we have all seen that it was very wise in this particular case. Had the specifications been written to tie down all the minute details, there would have been no improvement. We are all aware of the many improvements added to these *AN* connectors since they were first originated. It is only necessary to compare the products of various manufacturers to see what we mean. This method of writing specifications has resulted in both split and solid-type shells. The solid-type shells are without question more water-, dirt-, and oiltight, as well as vibration-proof and trouble-free. The split shells, on the other hand, permit wiring in closer quarters and have distinct advantages as far as time saving in installation is concerned.

Pressure-tight, moisture-sealed light-proof, explosion-proof and many other special design features have been added without deviation from the original specifications. Contacts that do not rotate and dielectric material with outstanding qualities have been added that are not included in the specifications. Again we would say that we like the way the specifications are written, because it has brought out all these desirable features. However, it might be the time now to review some of these features already mentioned and decide which ones might be included in the specifications as requirements.

Another *AN*-connector feature of importance is quantity of available styles and sizes. There seems to be confusion among some of the users because of the great number of connectors and connector combinations that are available. In reality the *AN*-connector series is very simple when one understands that it is made up of a small group of interchangeable parts.

For instance:

1. There are only four basic shell styles.
2. There are only 18 shell sizes.
3. There are but six contact sizes.
4. There are, however, 213 insert assemblies.

These shell styles, shell sizes, contacts, and inserts are so completely interchangeable that the number of possible combinations is in the thousands. We would like to point out that the shells can be divided into plugs and receptacles. The plug is always identified by the coupling ring and the receptacle by the external threads. Socket or pin inserts are interchangeable from plug to receptacle. It should be noted here that a plug is still a plug whether it has socket contacts or pin contacts. Any variations in shells is just in the shape or mounting, and it will be found that even the various component parts that are used in the shells are interchangeable from one assembly to another.

The 18 shell sizes not only permit a savings in space and weight but allow for the most economical use of the dielectric material.

All selections of connectors, of course, should originate with the contacts. The contact size is determined by the wire size. This in turn is placed in the dielectric or insert material, and the space or size of the insert itself is determined by the voltage considerations. With 213 inserts to choose from it is possible to find almost any combination of contact sizes and voltage arrangements one needs.

Another point that might be wise to discuss is the constant improvement made in these connectors by the various manufacturers as the war has progressed. The specification changes have not kept pace with the improvements. Coupling rings are now furnished with cross-drilled holes for safety wiring, as one example. Improved die-casting technique has resulted in more accurate die castings with less porosity. Gauging and inspection methods have been improved, and most of the manufacturers now inspect all parts 100 per cent on automatic machinery. This is particularly true of the contact pins and sockets where not only is automatic machinery used to check these parts dimensionally, but pullout pressures are checked on every part that goes into a connector.

Although the entire paper was interesting to us, we have confined our discussion strictly to the *AN*-connector series. We agree that a failure of any part of an electric system on the airplane is very serious, but we believe that *AN* connectors have earned for themselves a place in aircraft.

F. O. Stebbins and **L. A. Taylor:** In commenting on the discussion by Lieutenant Jerome H. Ehrich, the writers are in agreement regarding the objective of stripping the insulation without any damage to the wire but, up to the present time, have been unable to find such a requirement in any specification, nor could they obtain any written decision on this problem from responsible sources. Until the standards noted in this paper were voluntarily set up in view of available commercial wire strippers, insufficient attention to proper wire stripping was noted. With this standard, inspectors have a basis for rejection, and commercial stripping machines could be used for high-speed production. No failures have occurred in test or service that can be attributed to the standard.

It is hoped that information on the standard wire stripper which will not damage wire will be released as soon as possible.

Unfortunately, existing *AN* connectors on aircraft are frequently subject to direct rain or condensation. Severe corrosion of untreated aluminum die castings, meeting the present specification, has been noticed on many occasions. The writers believe that some type of protection should be required. Oil, gas, and hydraulic fluid on aircraft are also apt to come in touch with connectors so that protection against them is desirable.

Mr. Taylor at one time proposed a connector socket in which a coil spring of piano wire was used to support the socket. This could not be used in the particular application, because the phenolic inserts would have to be drilled out for the socket, resulting in too little spacing and creepage distance between contacts. It was particularly interesting to learn two years after this proposal that the Japanese were using a coil-spring support for their sockets.

The writers would like to call attention to the article, "Save Weight by Adding Wires," by H. F. Rempt, Lockheed Aircraft Corporation, in which the advantages of using two or more small wires in place of one of the large wire appear not only in weight saved, but also in smaller and simpler *AN* connectors and better-soldered joints. The use of small parallel wires would require special consideration as to wire markings.

Cable Used for Transmitting Electric Energy in Airplanes

Discussion of paper 44-223 by Melville F. Peters, John J. Phillips, Max Kronstein, and Helen Baldwin Jealous presented at the AIEE Los Angeles technical meeting, Los Angeles, Calif., August 29–September 1, 1944, and published in AIEE TRANSACTIONS, 1944, pages 1270–82.

W. Stanwix-Hay (General Electric Company, Bridgeport, Conn.): There are a number of items in this paper that are of interest to the electrical engineer engaged in the manufacture of power cables for airplanes, particularly at this time when we are at war. Because of this war, we are faced with the very practical problem of producing millions of feet per week of cable that is urgently needed by the Armed Forces and aircraft manufacturers. Because of this urgent demand, we must confine ourselves to practical insulation that can be applied at high speed, in large quantities, and with a minimum of defects in the manufacturing cycle and in the completed cable.

By inference, the authors of this paper attempt to show that wire insulated with a glass braid impregnated with varnish is superior to a vinylite-insulated wire. Vinylite is, of course, a trade name for a particular type of synthetic insulation and we suggest that the term polyvinyl chloride be used in this discussion, as, in this way, we do not limit it to any particular manufacture. Our experience has been that varnish or lacquer used as a binder for glass tape does not actually impregnate the tape but merely produces a surface film. Again, our experience has been that the varnishes suitable for use with glass have relatively poor bending properties, and it is for this reason that on flexible leads such as are used in aircraft we finish our wire not with varnish but with the best possible lacquers that we are able to procure. We have never been able to secure a varnish or lacquer, with one exception, that would operate at 230 degrees centigrade without deterioration. The exception that I mention is a very new development and can be obtained in such limited quantities that, at least for the present, it would be entirely impossible to manufacture aircraft cable with this purely laboratory product. Furthermore, it has been our experience that a glass tape, as we know it, presents such difficulties that it cannot be applied to wires having diameters as small as 0.032 inch which are used regularly in the wiring of our present-day airplanes. Although glass itself has high dielectric strength, a glass tape has so many interstices that in the presence of moisture the tape as a whole has very poor dielectric strength, and so reliance for dielectric strength must be placed on the lacquer or varnish film. If this is ruptured, moisture is quickly absorbed, and the dielectric strength of the tape is lowered. It is for this reason that we use the synthetic polyvinyl chloride on all of our present production of low-voltage aircraft wire.

We suggest that the authors specify the type of varnish used on this wire and also how long the wire operated satisfactorily at 230 degrees centigrade without apparent damage.

The authors propose a broad copper strip, apparently in lieu of the conventional round conductor that is used. To the best of my knowledge, such a strip could be made, if only solid wire were used, by simple magnet-wire machine equipment. If a flexible conductor is used, as is actually used in all types of aircraft-power wiring today, we know of only one method, and that is a braided type of conductor. This braided flat conductor presents numerous problems, the most difficult of which is that this type of conductor could only be manufactured on relatively slow apparatus that would, in our opinion, seriously retard the production program. Possibly the second most difficult problem would be the method by which we could satisfactorily insulate these strips in such a manner that we could insure a satisfactory insulation for potentials up to 300 volts and one that would conform to the many physical requirements that aircraft demand in their power and lighting systems. Also, if we see this picture correctly, a strip of this nature can only be bent in two directions. In other words, it simply does not have the flexibility that is required in conventional wiring.

Is it the authors' contention that bars could be used in lieu of flexible wire where cross sections equivalent to American wire gauge 22 and 20 are desired?

Joseph H. Jay (nonmember; Air Technical Service Command, Wright Field, Dayton, Ohio): The use of high-resistance cable for aircraft high-voltage ignition cable on aircraft engines may have its merit by decreasing wear by 50 per cent, but consideration should be given to the additional requirements which should be met by the cable for aircraft use. An increase in wear allowance would be voided if other properties of the cable such as dielectric properties, moisture absorption, tensile strength, and resistance to severe environments, were ignored. The design of high-resistance cable should be such as to conform with standard-size cables in present use on aircraft.

Increase of operating voltage in aircraft would aid materially in weight saving by the use of aluminum conductors in lieu of copper conductors. The present difficulty in present-day aircraft is that the majority of the electric equipment functions on low voltage and the equipment which operates on a higher potential consumes low current which would not affect an appreciable amount of saving in weight by the use of aluminum cable for smaller-gauge copper cable.

Operating temperature for the cable with the use of nonmetallic material would be decreased considerably, but because of the universal application of aircraft under severe environments the material used would have to be satisfactory under all conditions encountered.

The use of aluminum bars would alleviate trouble experienced in connecting aluminum conductors to appropriate equipment; however, the flexibility feature would be lost which is one asset that is badly needed in present-day aircraft to facilitate maintenance and installation. Another significant factor that should be thought of is vulnerability. The flexibility of electric cable in aircraft has aided many aircraft in arriving at its home base with a riddled fuselage but with the electric system still operating. With the use of rigid aluminum bars this advantage may be lost.

Carlos B. Mirick (United States Naval Research Laboratory, Washington, D. C.): The authors assume the weight of the electric-power source in aircraft in terms of weight per unit of power. A weight of 6.6 pounds per kilowatt is assumed for generators and an additional 6.6 pounds per kilowatt is assumed as fixed weight of engine. The use of quantity K_2 in subsequent equations implies the establishment of this or some similar value.

The values chosen by the authors seem entirely reasonable for the purpose intended. However, the establishment of a weight-to-power ratio suitable for practical applications is difficult. Not only do engine weights per unit of power and fuel rates per unit of power vary over a considerable range, but also it is not easily decided what items should be included in the charge. If one adds the weight of fuel and engine accessories and supports to that of the engine proper, the process can be continued until the "all-up" weight of the airplane is involved, in which case the plant weight per unit of power becomes the power loading of the airplane.

At the other extreme lies the view that the amount of power diverted from the main engines to drive electric equipment is relatively so small that engine weight may be neglected entirely. In other words, the primary source of electric power is treated as being one of infinite power and zero weight. At the present time, this second view is perhaps more generally acceptable than the first.

It is desired to emphasize the point that the establishment of some value of weight per unit of power for the power source is necessary for any weight-power analysis. Also, it may be noted that, once such a value is accepted, a size of conductor can be determined by means of Kelvin's law, by substituting weight for monetary value.

On this basis the optimum size of conductor appears as

$$A_{(\mathrm{opt.})} = \sqrt{\frac{K_2 I^2 a}{K_1}}$$

where

A = conductor area, circular mils
K_1 = weight of one mil-foot of conductor
K_2 = resistance of one mil-foot of conductor
I = current in amperes
a = slope of the weight-power characteristic of the power source

As in other applications of Kelvin's law, constant current is assumed, and no account is taken of heating limits or voltage regulation.

The comments expressed herein are the opinion of the writer and not necessarily those of the Navy Department.

Electric Gun Turrets for Aircraft

Discussion and author's closure of paper 44-195 by J. D. Thompson, presented at the AIEE Los Angeles technical meeting, Los Angeles, Calif., August 29–September 1, 1944, and published in AIEE TRANSACTIONS, 1944, November section, pages 799–802.

L. A. Zahorsky (General Electric Company, Fort Wayne, Ind.): The paper, "Electric Gun Turrets for Aircraft," is a well-organized and accurate presentation of the operation of this type of equipment.

There is a very interesting item in this paper which the author did not stress which might be of general interest to discuss.

Figure 2 of the paper gives a typical speed-response curve for a turret where a variable-voltage divider is used. This variable-voltage divider requires the addition of a potentiometer to be operated in tandem with the original control potentiometer, thus making the control complicated in comparison to the original simple fixed-tapped resistors previously used as a voltage divider. What are the advantages of this variable-voltage divider to offset this additional complication?

There is a very important condition of the system which must be maintained. This condition is called compensation or speed regulation of the turret motor with a change in load. If a voltage divider with two fixed taps and a sluing switch to change from one tap to the other is used, in conjunction with two rheostats on the Amplidyne, the compensation can be adjusted for both high-speed and low-speed operation of the turret. When a variable-voltage-divider circuit is used, only one rheostat can be used across the Amplidyne, as there are no switches operated when changing from low-speed to high-speed operation. This admits adjustment of the compensation of the system at only one speed of operation. Therefore it is a question whether or not this compensation can be maintained within the same limits over a wide range of speed as could be maintained in the original voltage-divider circuit with taps.

The writer mentioned that vibration is quite important insofar as it affects generator commutation, which is extremely critical for smooth turret operation in correct compensation. Steps have been taken recently to maintain a vibration of less than 0.0005 inch, as measured by a Davey vibrometer with the Amplidyne on a thick sponge-rubber pad, which in turn is placed on a steel block supported on springs. This is considered very low for 8,300 rpm two-bearing motor generator sets.

Harvey J. Finison (Equipment Laboratory, Wright Field, Dayton, Ohio): The 175-ampere power consumption referred to by Mr. Thompson may be somewhat misleading to those concerned with supply of sufficient electric power to operate the turret system. It should be noted that for the 0.5-horsepower drive motor described with an efficiency of 40 per cent for the over-all drive, the current input will be 34 amperes. The elevation and azimuth drives thus should have a total power drain of not over 68 amperes average (more exactly rms) over the ten-minute period of the drive rating.

Flight measurements under simulated severe combat operation show that the current consumption for the two Amplidyne units is slightly under 60 amperes. While peak power for any one Amplidyne was on the order of 70 amperes, there was sufficient diversity between even the two Amplidynes on a single turret that peaks did not approach 100 amperes. For a system of several turrets, operating peaks are still more diversified, and total current input is directly the sum of the average currents for the individual Amplidynes.

Thus, while it may be possible to operate a turret under such condition as to obtain a peak input of 175 amperes, it is believed that actual current consumption for the two Amplidyne turrets with associated accessory equipment will not exceed 100 amperes and will usually be somewhat less.

J. D. Thompson: Mr. Zahorsky asks the advantages of a voltage feedback ratio continuously varied by a potentiometer in tandem with the control potentiometer, that is, operated by the control grips, over the formerly conventional method of obtaining sluing characteristics by changing taps on a fixed resistor. The latter arrangement assumes that all target tracking is done in the normal speed range and that it never is necessary to use sluing speeds for that purpose. This is fallacious, and with this arrangement a gunner who found he had to shift to the sluing tap in order to keep up with his target would lose the target because of the jump in turret speed when the changeover was made. The most desirable speed-control characteristic is one where relatively large handle movements are required for a given speed change in the region of low speeds, with a gradual transition in the slope of the curve so that small handle movements give large speed changes in high-speed regions, as in Figure 2. Presumably, the ideal characteristic curve would be one which varied in the same proportion as the variation in the required angular velocity of the turret as the target approached from a distance at a constant speed. Then a constant-control-movement velocity on the part of the gunner would keep him on the target. This, however, is a variable, depending on the target path so that a compromise curve must be used.

Mr. Zahorsky also questions this from the standpoint of maintaining proper Amplidyne compensation required for good speed regulation under varying wind load. Actually, it has a beneficial effect. As Mr. Zahorsky indicates, when sluing is obtained by a switch to reduce the feedback ratio, it is necessary to reduce the Amplidyne compensation at the same time by decreasing the shunt resistance, as otherwise, at low-voltage outputs, there would not be sufficient feedback to overcome the previous compensation. However, at high-voltage outputs, the necessity for the adjustment vanishes since there is plenty of feedback voltage available, and the fact that it had to be made to take care of slow-speed operation in the sluing circuits means that the Amplidyne is undercompensated at the higher sluing speeds. With the continuously varying feedback ratio, however, it is not possible to operate at a low speed in a sluing connection, so that the change in compensation adjustment does not have to be made. This means that the full compensation is available at top speeds, so that good speed regulation can be maintained to higher percentages of top speed than possible with the other type of circuit arrangement.

Mr. Finison's comments are correct and most appropriate. For completeness however, it should be mentioned that the actual peak-current figures will vary rather widely, depending on the speed of the airplane and the location of the turret in it. Also the 40 per cent efficiency quoted in the paper was exclusive of gear efficiency. The application for which these particular Amplidynes were first designed had a gear efficiency of about 65 per cent and a windload requiring 0.7 horsepower at top speed, corresponding to a $1^1/_2$-minute rating of the motors and an Amplidyne input of over 70 amperes.

Some Aspects of the Application of Induction Motors to Aircraft

Discussion and author's closure of paper 44-198 by H. J. Braun, presented at the AIEE Los Angeles technical meeting, Los Angeles, Calif., August 29–September 1, 1944, and published in AIEE TRANSACTIONS, 1944, October section, pages 769–72.

W. A. Barden (major, United States Army, Wright Field, Dayton, Ohio): The paper under discussion very effectively presents most of the aspects of the application of induction motors to aircraft. However, in addition to the operating conditions and performance characteristics involved, there is the very important consideration of the exact manner in which the motor and the driven equipment are combined.

For example, should the motor be built as an integral unit which is attached to the driven device, or should every effort be made to standardize a line of three-quarter motors which could be built into the driven device, thus facilitating a design which approaches the ultimate in compactness, light weight, and reliability?

Perhaps it would be well at this point to define what is meant by a three-quarter motor. The usual squirrel-cage induction motor consists of four principal parts, that is, the rotor, the stator, and the front and rear end brackets. The so-called three-quarter motor would eliminate the rear bracket and would mount directly to the driven equipment, making use of a design which would eliminate the rear bracket and bearing which are required in a motor built as an integral unit.

It is felt that such a design would be of sufficient worth from the standpoint of reliability, simplicity, compactness, and light weight, to be of very great interest to the aircraft-equipment designer who seeks to make his device as reliable, compact, light in weight, and simple as he possibly can.

There have been numerous discussions pro and con regarding the three-quarter motor with the net result that certain individuals still feel that the design has merit while the manufacturers feel that its disadvantages far outweigh any possible advantages.

However, a number of applications have been developed recently in which only the stator and rotor are designed into the driven equipment, and an integral motor as such does not exist anywhere along the line. It is believed that this design is the ultimate in the common goal of all designers of aircraft equipment, namely, the lightest in weight, the most compact, and, last but not least, the most reliable equipment which can be built. Time alone will show whether the many objections which will be raised in opposition to this and the three-quarter motor design can be overcome.

The author has discussed the torque–speed characteristics of the induction motor and has shown that most applications can readily be satisfied using the induction motor driving its load either directly or through a gear train. It is believed that by the use of a suitable variable-ratio torque–speed converter in conjunction with an induction motor all requirements for motion of control surfaces, gun turrets, landing gear, and so forth, can satisfactorily be accomplished and with all the features of ruggedness and reliability for which induction motors are so well known still being retained.

The author is to be commended for depicting so clearly the problems involved in the application of induction motors to aircraft and for outlining the performance characteristics which are important in such applications.

Cyril G. Veinott (Westinghouse Electrical and Manufacturing Company, Lima, Ohio): Mr. Braun has done an excellent job in describing the characteristics of induction motors, comparing them with the more familiar d-c series motor. His speed–torque curve for a three-phase four-wire motor with one line shot away is particularly fascinating. Under such conditions a zero-sequence voltage would be impressed upon the motor and, of course, zero-sequence currents would flow through the stator windings. Zero-sequence currents flowing through a three-phase winding set up a single-phase field having three times as many poles as the fundamental. Thus it is to be expected that the characteristic speed–torque curve of a single-phase induction motor will be superimposed upon the normal speed–torque curve of the motor; this single-phase speed–torque will have a synchronous speed one-third that of the normal winding. Moreover, a single-phase induction motor normally has much more breakdown torque as an induction generator than as a motor. Examining Mr. Braun's curve in Figure 6 of the paper reveals the characteristics just described, except that the curve appears to show a synchronous rather than induction cusp. Perhaps Mr. Braun will state if he actually found a synchronous cusp, or if this is merely a discrepancy in plotting up the curve. If this is the correct explanation, this single-phase cusp can be completely removed by making the pitch factor for the third harmonic equal to zero: that is, by using a pitch of $^2/_3$ or $1^1/_3$.

The loss in breakdown torque is mainly caused by the reduction in positive-sequence voltage due to opening one line. Does Mr. Braun confirm this opinion?

H. J. Braun: Major W. A. Barden has raised a very controversial issue in his discussion: namely, three-quarter motors. There are many arguments pro and con on this subject. Major Barden has presented most of the arguments which favor the three-quarter motor. While it was not the purpose of this paper to enter into this controversy, the most important argument against these motors, namely, division of responsibility, should be mentioned. This division of responsibility comes in at three important places in aircraft motors—bearing fits and location, mounting fits and tolerances, and cooling. These problems are not insurmountable. There is no question that, if the demand for sufficient quantities of such motors develops, somebody will build them.

Mr. Veinott has questioned the cusp which appears in Figure 6. This is a single-phase induction-motor cusp of the third-harmonic flux. This set of curves was taken by test, and, because of the extreme heating which occurs while taking this part of the speed-torque curves, relatively large errors creep in. This cusp should have occurred at about 33 per cent of synchronous speed, but, because of the speed with which measurements had to be taken, this test curve shows it at approximately 30 per cent of the synchronous speed. Mathematical analysis shows that a similar cusp should occur in the curve, Figure 6, showing single-phase operation. Because of the size of the measuring equipment and because of the heating that occurs, this second cusp was not detected.

When one line is opened, the loss in breakdown torque can be attributed to the following facts: reduction in positive-sequence voltage, the negative torque caused by the negative-sequence currents, and the negative torque caused by the zero-sequence currents.

One Type of Rotary Magnetic Clutch and Its Associated Brake Used on Aircraft Electric Motors

Discussion and author's closure of paper 44-207 by Leo Andrews and Fred Shanely, presented at the AIEE Los Angeles technical meeting, Los Angeles, Calif., August 29–September 1, 1944, and published in AIEE TRANSACTIONS, 1944, December section, pages 893–5.

H. J. Braun (Westinghouse Electric and Manufacturing Company, Lima, Ohio): For aircraft use, brakes and brake and clutch mechanisms must be light in weight, trouble-free, and positive in action. Any mechanism that does not meet these requirements is doomed to eventual failure.

The authors in this paper have opened an interesting subject. It is hoped that they have attained their objective in instigating more research and study on these mechanisms. No doubt today all manufacturers of motors for aircraft use are investigating and building a certain number of brake units. Many are working with combination clutch and brake mechanisms. It is hoped that they too will see fit to publish some of their engineering data for the benefit of the profession.

In most applications of brakes and brake and clutch mechanisms to aircraft motors, there are two main objectives: stopping the load in a given length of time and holding the load.

Stopping the load in a given length of time is the main consideration in most control mechanisms where accurate positioning is desired and the actual amount of load is small. For this type of operation, a brake and clutch unit is usually preferred. The clutch makes it unnecessary for the brake to stop the armature of the motor. The brake therefore may be much smaller than if it had to stop the load and the armature.

The other consideration, holding the load, becomes in many cases the main consideration in actuator and hoist application. In a great many of these applications the holding torque that must be furnished by the brake governs the size of the brake and is of such magnitude that the brake will stop the load and the armature easily within the required time. Thus, it is unnecessary to complicate the structure with the addition of a clutch.

In some applications the brake must stop an overhauling load as well as provide adequate holding torque. For such loads a clutch mechanism which removed the necessity of stopping the armature definitely would aid the brake.

The final governing fact as to whether a brake or a brake and clutch mechanism is to be used is weight. A great deal of study and research for the particular job is necessary to determine this. Some of the factors that must be considered have been covered here. Further additional publications will bring out many others which have been uncovered by extensive experience in the field.

C. H. Spitler (The Leland Electric Company, Dayton, Ohio): Having spent considerable time on the design and development of magnetic clutches and brakes such as the design outlined in this paper, I believe the authors should be complimented on their excellent presentation of the basic principles of operation of such a device.

There are several items which could be considered more minutely, one of these being the leakage flux. For instance, on part D, generally known as the coil core, Andrews and Shanely did not state that part of this flux did not cross the air gap between parts D directly; but instead, a fair part of this could be considered crossing the air gap from the upright portion supporting the coil to the back of the drive disk and also through the hub of the drive disk. Usually the shaft extension of the motor engaged with the drive disk is considered part of the magnetic path. This condition is not taken into account during the discussion by the authors; however, it could be accepted wherever a steel shaft is used.

The part E is indicated as having a flux density of only 44 kilolines per square inch, which is a factor worth considering when one is interested in weight saving. However, this part usually cannot be decreased in cross section to accommodate a higher magnetic density because of the mechanical requirements which demand a high degree of rigidity.

The air gap between parts A and B, drive disk and driven disk, is not considered in the general analysis. This gap, although small, is highly reluctant and should be given due consideration. The formulas outlined for the force necessary to close these two disks allow for the reluctance of this gap. The formula

$$F_1=\frac{B^2A}{72\times10^6}$$

and then transposed to

$$B=\sqrt{\frac{F_1\times72\times10^6}{A}},$$

where F_1 is the force of the spring in its initial position with the driven disk against the brake could be improved for greater accuracy. It is assumed that the flux required to hold these disks together is proportional to F_1+F_3, where F_3 is the frictional force to transmit torque. Since the spring is acting at one compression stage with disks A and B separated, it appears that the total force then would be equal to F_1+F_4 (additional force due to increased spring compression when disks are in contact). This equation would then become

$$B=\sqrt{\frac{F_4+(F_3+F_1)72\times10^6}{A}}$$

Such a correction would conform with Hooke's law, which states that within the proportional limit of any material, deflection is proportional to load.

The foregoing correction also could be carried through into the formula for disengaging time, where

$$t=\sqrt{\frac{2dM}{F}}$$

If X_2 = maximum position of spring compression and X_1 = minimum position of spring compression, and K is a constant pertaining to the compression force to deflect the spring a unit length, then:

Since

$$\text{Work}=\text{force}\times\text{distance}$$

$$\text{Average force}=\frac{\text{total work}}{\text{distance}}$$

$$\text{Total work}=\int_{X_1}^{X_2}F\,dX$$

$$=\int_{X_1}^{X_2}KX\,dX\ (\text{distance}=X_2-X_1)$$

$$\text{Average force}=\frac{K\int_{X_1}^{X_2}X\,dX}{X_2-X_1}$$

$$=\frac{K\left(\frac{X_2^2}{2}-\frac{X_1^2}{2}\right)}{X_2-X_1}$$

Substituting in formula

$$t=\sqrt{\frac{2dM}{F}}=\sqrt{\frac{2dM}{\frac{K\left(\frac{X_2^2}{2}-\frac{X_1^2}{2}\right)}{X_2-X_1}}}$$

This method is a more exact procedure, although in most cases it will not be necessary unless the spring is working outside its elastic limit.

Other variations of this clutch might be the use of a magnet coil in place of the conical spring which would allow manual operation of the driven device by disconnection of current flow through the brake coil.

A design of such a unit has been made with satisfactory results, although the weight and size may be increased slightly to accommodate such construction.

It also might be well to mention that a series-type clutch is not so conducive to efficient motor performance as compound-type clutch winding, which would allow the copper loss and voltage drop across the winding to be decreased, resulting in less loss and higher voltage across the motor proper. Some experimenting is being done at the present time to incorporate such windings wherever possible in order to improve unit performance, particularly under overload conditions.

Fred Shanely: It is appreciated very much that Mr. Braun has placed additional emphasis upon the possible benefits to the profession if other manufacturers will publish engineering and research data upon this or correlated subjects. This is especially true, because the practicability, endurance, and worthiness in general of such a mechanism have been amply demonstrated by its more than four years' field service on some of our largest and fastest war airplanes. Those who have studied, analyzed, and applied it have found its potentialities virtually unlimited. Of all motors manufactured in the two plants of the company 95 per cent are built with one or another type of clutch-brake mechanism as an integral part of them.

In holding the load, this being quite often the major objective in the design of a power package, as stated in our paper and reiterated in Mr. Braun's discussion, several factors may enter into the over-all design that make for the control of the size of the braking part of the twin unit. For instance, there is the "No-Bak" one-way gear lock, developed and used principally by automotive manufacturers, that can be used to hold any part or all of the total load, in the latter case entirely eliminating the necessity of a brake insofar as holding is concerned.

Then again a gear (especially a worm or lead screw) may be designed with such a pitch that it, itself, becomes the major holding element.

To summarize, it is known that the idea of braking a load at a point of low inertia, namely, between the motor and first gear train, is basically sound and valuable. Trouble-free operation over a period of years has proved the merit of the clutch-brake "twin" and will continue to do so, refined by experience, in future war and postwar applications.

Finally, when it is borne in mind that a small motor acting through a gear train can lift more than 400 times its own weight and yet have the actuator it is driving braked to standstill in $^1/_{60}$ to $^1/_{30}$ of a second, the weight of the clutch-brake mechanism, which is incorporated to make these things possible, is rather negligible.

This is intended to be rather in the form of an acknowledgment than a closure, as it is felt that Mr. Spitler's remarks are very timely and appropriate. It is fully realized that, because of the press of home-front developments, research and calculation refinements were not carried out to the points desired. Mr. Spitler's last paragraph might be touched upon, inasmuch as the use of a compound-wound clutch, while conducive to better motor performance, tends to complicate and add weight to the control system, over that of a straight series clutch. (It must be remembered that by far the greater percentage of motors with the integral clutch-brake are reversible.) Of course, whichever consideration is paramount will determine the final choice of a clutch winding.

An Analysis to Determine the Optimum Bussing Arrangements and Transmission Capabilities at Grand Coulee

Discussion and authors' closure of paper 44-215 by B. V. Hoard and G. W. Bills, presented at the AIEE Los Angeles technical meeting, Los Angeles, Calif., August 29–September 1, 1944, and published in AIEE TRANSACTIONS, 1944, pages 1259–64.

E. W. Knapp (The Shawinigan Water and Power Company, Montreal, Quebec, Canada): There are a number of points pertaining to this excellent paper which I believe could be discussed to advantage. Possibly a record of distances between the main 230-kv stations would prove of value to those readers not familiar with the area under consideration.

1. What is the power-factor rating of the generators at Grand Coulee? Was any a-c analyzer study made to determine the most suitable power-factor rating, WR^2 characteristics, and transient-reactance characteristics, prior to the purchase of these generators? Are there any special features pertaining to the design of the generators, or was a standard design adopted in this case?

2. What is the range of operating voltage on the high-voltage busses at Grand Coulee?

3. While operating under the normal arrangement, is there a tendency to deliver a large block of reactive energy to Spokane and Midway while only a limited amount of reactive energy is delivered over the Covington lines?

4. Is it the intention to construct, in the future, one or more 230-kv tie lines interconnecting the Spokane and Midway stations?

5. If the station were operating as shown in Figure 7, and one of the Midway lines were opened on fault, would this tend to overload the 115-kv tie lines between Grand Coulee and Spokane and between Spokane and Midway?

6. During the period when only one Covington line was in service, with two large generators, a fault on the 115-kv system resulted in loss of synchronism of this single 230-kv-line system. Would this also apply with one large and one small generator on the 230-kv line and 5.75-cycle clearances of the 115-kv fault?

Ferber R. Schleif (United States Bureau of Reclamation, Coulee Dam, Wash.): Some of the more practical aspects of the problem described by this paper from the Bureau of Reclamation's construction and operating standpoint are offered to make the picture more complete.

The paper states among conclusions that an important feature of the interleaved bussing arrangements is the ease with which a change-over can be made from the normal interleaved to an emergency interleaved scheme, as no complex switching operations are necessary. While this is true, particularly from the authors' viewpoint after having studied numerous alternatives of considerably greater complexity, the best solution to this problem still involves some interesting problems in switching, many of which arise from the wartime necessity for providing an adequate operating arrangement with a minimum of equipment and critical material.

Many engineers will be interested in the mechanics of switching. The single-line diagram of Figure 1 of this discussion shows the physical arrangement of the switchyard and connections for normal fully interleaved operation. Figure 2 shows the switchyard arrangement for an emergency interleaved scheme of operation. The switching may be accomplished in the following sequence:

1. Open breaker 28.
2. Open disconnect 2*B*.
3. Close disconnects 7 and 34.
4. Close breakers 20, 26, and 30.
5. Open disconnect 1*B*.
6. Open breakers 14, 19, 25, and 29.
7. Close breaker 13.

The switching is controlled by the following considerations:

A. Only two of the six 230-kv lines have circuit breakers to both busses.

B. Bus sectionalizing is accomplished only with air-break switches, no bus oil circuit breakers being provided.

C. No more than four large generators and three 230-kv lines may be bussed together during switching.

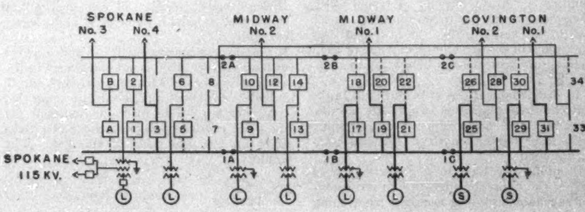

Figure 1. Normal fully interleaved scheme of operation

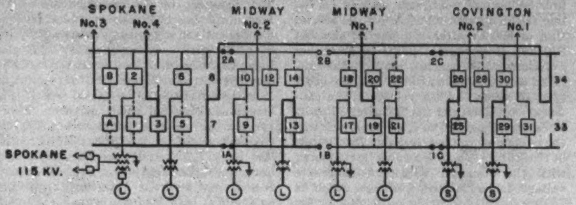

Figure 2. An emergency interleaved scheme of operation

D. It is undesirable to leave any line connected to a bus section which has no grounded transformer bank.

The paper mentions briefly the automatic dropping of generation upon occurrence of line faults in order to maintain system transient stability. The flexibility and simplicity of the scheme provided for accomplishing this function will be of interest. The scheme consists of a series of control selector switches, one for each line, for selecting which of three generators connected with the scheme is to be tripped automatically with operation of a line breaker or combination of line breakers. For safety, the scheme is so arranged that the trip-circuit interconnections may be conveniently isolated when not in use.

Some readers may be confused as to the relation between the Government agencies controlling parts of the system. Grand Coulee power plant, of the Columbia Basin project, a reclamation development, was constructed and is operated by the United States Bureau of Reclamation. Bonneville power plant, with which navigation of the lower Columbia River is connected, was built and is operated by the United States Army Engineers. The transmission system is built and operated by the distributing and marketing agency, the Bonneville Power Administration. These component parts are operated as one large system.

B. V. Hoard and G. W. Bills: The discussion by Mr. Schleif of the Bureau of Reclamation has included several important factors concerning actual operations at Grand Coulee, which were not fully covered in the paper, and we feel it will materially add to the paper's value.

Mr. Knapp's discussion has brought up a number of interesting points, which will be reviewed by correspondingly numbered paragraphs in the following. It might be mentioned that a geographical map showing locations and approximate lengths of lines between stations was omitted from the paper for national-security reasons.

1. The Grand Coulee generators are rated at 100 per cent power factor. In connection with this rating, consideration was given to the fact that it is very uneconomical to transmit reactive power to the load through long transmission lines, such as from Coulee to Covington (Seattle), 183 miles, and from Coulee to Ross (Portland), 270 miles.

Before the first Coulee generator was obtained, consideration was given by the Bureau of Reclamation to the effects of increased WR^2 and reduced transient reactance on improved stability. This is shown by the fact that the WR^2 of the generators is 5.5 kw seconds per kilowatt of rated capacity, and the transient reactance of the generators is 22.5 per cent. However, by far the largest single improvement in stability is obtained through use of carrier-current relays and high-speed three-cycle circuit breakers, as compared to eight-cycle breakers and sequential clearing of faults.

2. The 230-kv bus voltage at Grand Coulee, as a direct result of the war, operates in a range between 240 and 245 kv, or a maximum of six per cent above the 230-kv equipment rating. During normal operation it has been necessary to obtain maximum output from both equipment and lines. This has included generator overloads up to 120 per cent of rated value, essentially maximum excitation, the highest operable voltage on transmission lines, and on the Covington line a loading from 190,000 to 230,000 kw.

3. There is no tendency for Coulee to deliver excess reactive kilovolt-amperes to Spokane and Midway because of high-side taps on the Spokane and Midway transformers. At Spokane there are also two 35,000-kva synchronous condensers. The amount of reactive kilovolt-amperes delivered to Covington from Coulee is, of course, limited by the long transmission lines and the allowable minimum operating voltage at Covington. However, sufficient reactive kilovolt-amperes is obtained from the other agencies interconnected with the Bonneville Power Administration system at Covington, together with a 35,000-kva synchronous condenser installed there.

4. There are no immediate plans to connect Midway and Spokane stations by a 230-kv line. The transmission capacity in these areas seems adequate, as indications are that most future load growth will be in the Seattle and Portland areas.

5. In reference to the bussing arrangement of Figure 7 of the paper, removal of a faulted 230-kv Coulee–Midway line, of course, would produce some increase in load through the 115-kv tie lines between Coulee and Spokane, but sufficient transformer capacity is available for the increase. However, the load flowing out of Midway on the 115-kv lines, would be decreased by removal of a Coulee–Midway line. The largest load increase occurs in the remaining 230-kv Coulee–Midway line, since a majority of the load normally in these two lines is transmitted to the Portland (J. D. Ross substation) area.

6. A reduction in the power normally transmitted over the Covington line, materially improves the chances of its remaining in synchronism following the occurrence of a fault on another line in the vicinity of Covington. With one large and one small generator on the Covington line and operating at maximum output, a reduction of 35,000 kw occurs in line loading as compared to when two large generators are operating on the line. This reduction in loading would prevent loss of synchronism, for faults on 115-kv lines in the vicinity that are cleared in 5.75 cycles.

New 138-Kv Cable Lines in Los Angeles

Discussion and author's closure of paper 44-226 by Carroll G. Mansfield, presented at the AIEE Los Angeles technical meeting, Los Angeles, Calif., August 29–September 1, 1944, and published in AIEE TRANSACTIONS, 1944, pages 1240–9.

D. M. Farnham (Quebec Hydro Electric Commission, Montreal, Quebec, Canada): The author is to be complimented on his paper, especially for the way he has dealt with many of the details not previously covered in the various papers we have had on high-voltage cable work.

The duct arrangement is novel, and it would be interesting to know what percentage increase in cost of duct-line construction resulted. With conventional duct sections carrying two circuits of 120-kv cable we have been unable to notice any unbalanced effects. Our practice is to carry all pilot and signal cable in an entirely separate duct line; thus the problem of induced voltages does not exist.

Mr. Mansfield's treatment of cable movement due to temperature cycles seems very reasonable and quite in line with our experience. Where we have lengths up to 1000 feet and have installed movement gauges in the manholes, it would appear that approximately the middle third of the stretch remains stationary, while the end thirds move. The daily movement in the manholes is between one-eighth and one-quarter inch, while the weekly movement runs up to one-half to three-quarters inch. Our opinion is that these movements are well within safe limits.

The omission of fireproofing in manholes shows a saving of only a very small fraction of a per cent on the total job cost. My belief is that, although fireproofing may be of only small use in case of fire, yet it provides a protection for the sheaths in manholes where accidental damage is most liable. The added rigidity and reinforcement is well worth while, even if the fireproofing value is nil.

I am very much interested in the author's paragraph on cable vibration. We have the same phenomenon and were quite disturbed about it initially. The author suggests that it may be due to loose outer strands in an electrostatic field. Since our cables (oil-filled) are all made up of segments rather than strands, this theory would not apply, and further we have noticed that the points of maximum vibration appear to move from one place to an-

other. Our conclusion some time ago was that the vibration might be caused by the spiral steel core which is terminated outside of each joint and may also be free to vibrate at bends in the cable. At some locations the vibration is audible and noticeable near metallic hangers used to support the cable.

In conclusion, I would like to ask the author how he detects a cell leak in a *CC*-type reservoir.

R. W. Atkinson (General Cable Corporation, Bayonne, N. J.): The design of a transmission system for carrying large power loads at high voltage requires a good deal of careful engineering design. This is true, even though the fundamentals already have been well covered in the various references cited by Mr. Mansfield. The assembly of these, showing their application to a specific system, is a distinct contribution. It is further useful to have these general considerations shown in their relation to the specific problems of a given system and to have a description of the number of new developments that are described in this paper.

The arrangement of the ducts approximately around the surface of a cylinder is a departure that seems to have considerable merit.

That the major part of the cost of using an insulation thickness greater than standard is paid for by the increase in current-carrying capacity is interesting and significant. It is brought out that this is, in part, due to the unusually low thermal conductivity of the soil. In calculating the value of the increased carrying capacity it would have been justifiable to take into account that the increase in carrying capacity affects the cost per kilowatt not only of cable and accessories but also of the duct system. On this basis, a still greater portion of the cost of the extra thickness of insulation is paid for by the increase in current-carrying capacity.

It is noted that the carrying capacities were calculated on the guaranteed values of cable dielectric loss and not on the considerably lower measured values. The choice of course is optional with the design engineer, but the use of the guaranteed value is not classifiable as ultraconservative, because the existence of the guarantee is in itself an indication that there is a possibility, even though it be a very remote one, that the guaranteed loss may be encountered at some time in factory or field. On the other hand, should the losses not exceed the measured value, as seems likely on the basis of more recent data, the thicker insulation would make possible in the future considerable increase in power-transmitting capacity by an increase of operating voltage. Should occasion for this arise later, the thickness of insulation used will have been justified many times over.

In connection with possible future operation at 162 kv, it is to be noted that tests on cable and accessories were what would have been standard for these if intended for use at that voltage. This may be illustrated by some of the test data obtained on stop and normal joints such as used on two of the three circuits described by Mr. Mansfield. The scheduled surge tests consisted of approximately 25 shots at about 650 kv, followed by a like number of shots at each voltage increment of 10 per cent until failure. A normal joint such as used on the line failed on this schedule on the second shot at 920 kv after standing 22 shots at 870 kv. Two normal joints of an earlier experimental design, not used on the line, failed at 750 kv, one after 13 and the other after 23 shots at that voltage. The condenser stop joint did not fail after 30 shots at voltages from 980 to 1,000 kv, which was the maximum voltage available for the test at that time. These tests may be compared with the standard withstand surge-test voltage for apparatus bushings which is 750 kv for 161-kv rating and is 900 kv for 196-kv rating.

These particular circuits happen to be the highest-rated cable circuits in this country. American manufacturers, however, have long been in position to furnish cables and accessories for operation at 220 kv or more, if called upon to do so. The company with which the writer is associated has made and tested cable designed for that voltage and about ten years ago supplied to a French company two condenser joints for use in testing 220-kv cable which was installed in Paris.

D. M. Simmons and **M. H. McGrath** (General Cable Corporation, New York, N. Y.): The rating assigned by the author to the 1,500,000-circular-mil size is 125,000 kva for two loaded circuits at 25 degrees centigrade earth ambient temperature and 62.5 per cent daily loss factor. For the same governing conditions, calculation using the Edison Electric Institute duct constant along with Simmons' method and a 0.75 per cent power factor, gives about a 40 per cent higher rating. This is a relatively large difference, and, in view of the author's statement to the effect that the rating does not include any unreasonably large safety factor, it may be of interest to review briefly some of the more important steps of the author's calculation procedure. In principle at least the method used follows that suggested by Atkinson,[2] Simmons,[1] Kirke,[3] Halperin,[4] Whitehead and Hutchings,[5] and others. In other words, the heat-flow path from the conductor to earth sink is broken up into a series of component parts and the thermal resistance of the individual parts determined and combined.

The author's equation 2, which applies to the path from conductor to sheath, is accepted generally and requires no comment.

While equation 3 is stated at one or two points in the text to apply to the heat-flow path from sheath surface to duct air, it seems obvious from other portions that it is the author's intention to employ this equation for the path from sheath surface to the inner wall of the duct rather than to duct air. Simmons, on the basis of tests by Atkinson, uses an identical expression for this component and suggests a surface resistivity of 1,200 (watt-centimeter units), this also being the value used by the author. Kirke employs the same formula but recommends a surface-resistivity constant which decreases as the cable loading increases. However, for the load used by the author, Kirke's constant would be only about 5.5 per cent less than 1,200 and consequently would not alter appreciably the over-all result. Rosch[6] has reported experimental results on the heat transfer from cable-sheath surfaces to free air and from conduits, with cable located within, to free air. By a simple combination of Rosch's recommendations for these two cases it is found that the thermal resistance from cable surface to the inner wall of a 4.5-inch conduit is 0.5 degree centigrade per watt per foot (or 33 per cent) greater than that used by the author.

Halperin also uses the same expression as the author along with a surface-resistivity constant of 1,200 for cable diameters over 3.0 inches. In his case Halperin considers equation 3 with 1,200 for B to represent the thermal resistance from sheath surface to the air in a center empty duct. Halperin also gives a figure of 550 for B for use in finding the thermal resistance from cable surface to duct air. Obviously the thermal resistance from cable surface to duct wall must be between these limits. As the greater part of the temperature drop between the loaded-duct wall and the center empty duct probably occurs in the immediate vicinity of the loaded duct, it seems reasonable, in line with the suggestion made originally by Atkinson regarding the relationship between the temperature drop to a loaded-duct wall and to an adjacent idle duct, to assume that Halperin's figure of 1,200 also could apply to the wall of the loaded duct without introducing any important error.

The third partial thermal resistance, namely that from the inner wall of the duct to the outer surface of the concrete envelope has been determined experimentally by the author to be 1.48 degrees centigrade per watt per foot of cable. This is lower than the 1.95 figure used by Kirke for the same path. However, any comparative figures for this term are of little significance unless the details of the duct-bank construction are taken into account as the resistivity of the duct material used, duct diameter, spacing, and so forth, may alter appreciably the thermal resistance of this path.

Equation 4 which is given by the author for the heat-flow path from the outer surface of the concrete envelope is in reality Kennelly's full equation in a form, believed to

Table I

Source	Reference Points	Soil Condition	Duct Constant*
Mansfield	Loaded-duct wall—earth	Apparent resistivity—110	1.47
EEI	Not stated	Average	0.82
Halperin (summer)	Idle duct—earth	Fair	1.17
Halperin (winter)	Idle duct—earth	Fair	1.04
Kirke	Loaded-duct wall—earth	Average	1.33
Kirke	Loaded-duct wall—earth	Poor	1.63

* Degrees centigrade per watt per foot of duct-bank structure for duct bank with six loaded outside ducts.

have been originated by the author, which is very well adapted to quick and accurate slide-rule evaluation in spite of the cut-and-try procedure required. This equation also can be shown readily to be identical to that used by the British (Whitehead and Hutchings) for both buried and duct cables. In addition the author applies it in exactly the same way as the British, namely, by using the area of the duct structure to determine the radius of the equivalent cylinder. It is particularly gratifying to note that the author has not had to depend upon using a soil-resistivity constant determined by sphere, cylinder, or similar method in the cylinder-image type of formula. Instead the author has determined an apparent resistivity from test results on an experimental duct bank by means of the same equation subsequently used for calculations on the new 138-kv line. With this value of apparent resistivity and a corresponding measured moisture content the author is then able to orient resistivity-moisture data taken under other conditions so as to obtain a fairly reliable index as to the apparent resistivity to be used in equation 4 for other moisture contents. For the 138-kv line the author has used an apparent resistivity of approximately 110 (watt-centimeter units) as corresponding to six per cent moisture content.

In Table I the duct constant, that is the thermal resistance from loaded-duct wall to earth, used by the author is compared with constants given by others. Judging from this table the duct constant used by the author cannot be considered too high for the relatively poor soil conditions indicated. It also appears that the EEI constant for a six-duct bank corresponds to very favorable soil conditions. If the author's figure is used as a bench mark it is found that the EEI constant corresponds to an apparent soil resistivity of approximately 50 (watt-centimeter units) for a four-foot cover and approximately 70 (watt-centimeter units) with a two-foot cover.

Under the circumstances it would appear that the author is fully justified in considering that the rating assigned to this cable is not overconservative for the soil and other conditions met with in Los Angeles.

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I. T. Faucett (General Cable Corporation, New York, N. Y.): Mr. Mansfield has made a very fine contribution to the literature on oil-filled cable, and the system described speaks highly for the engineering talent responsible for the many unusual design features involved.

I particularly like his method of computing current-carrying capacity by eliminating the conventional duct constants and substituting the thermal resistance of the duct structure and the earth. This method permits more flexibility and allows separate and specific consideration of the thermal resistivity of the earth. The variation of this thermal resistivity may be very large over the cable route at any one period of the year because of the different types of soil that may be encountered, difference in drainage or ground-water level, and difference in surface conditions such as street paving. Likewise at any one point on the cable route the soil resistivity may vary over an appreciable range because of change in moisture content of soil throughout the year associated with amount and distribution of rainfall and other climatic conditions. I know of one case where along a seven-mile circuit the variation in soil resistivity at one time of the year ranged from 80 to 120 watt-centimenter units, and at one location it ranged from 63 to 103 watt-centimeter units in a three-month period. The effect on current-carrying capacity is so great that the thermal resistance of the earth should be considered a separate component, as Mr. Mansfield has done.

Figure 2 of the paper shows a large variation in the thermal resistivity of soil as a function of moisture content. Several investigators have measured resistivity without measuring the moisture content of the soil. Judging from the curve both measurements are essential in even the simplest definition of the thermal properties of the soil, and this will be particularly true if data obtained at one location are to be applied to another.

I wonder whether Mr. Mansfield has made a temperature survey of the system to see whether the calculated values have been proved in actual operation.

Mr. Mansfield's treatment of lead strain and sheath life is the latest approach to this important subject. Although copper-bearing lead such as used on the Los Angeles cables has been the standard for oil-filled cable for almost ten years and has proved eminently satisfactory, it may be of interest to mention briefly a new ternary alloy composed of copper–lead and a small percentage of tin, which further improves the already very good properties of the ordinary copper–lead binary alloy, one of these being fatigue resistance. The difference in performance is brought about by the presence of the tin which tends to promote homogeneity in chemical composition at the tongue and weld regions of the lead sheath. In a sheath composed of the conventional copper-bearing lead or chemical lead there is a distinct segregation of copper or copper-rich alloy which is concentrated in the tongue and weld region and thereby produces certain mechanical weaknesses. The addition of a small amount of tin (0.10 per cent) prevents this segregation, thus producing a more homogeneous structure and one which is not locally embrittled by regions abnormally rich in copper as in the binary lead-copper alloy. The tests have shown that the ternary alloy lasts approximately 50 per cent longer than the binary alloy when subjected to a given amplitude of vibration.

The designs of the duct run and manholes shown by Mr. Mansfield are a departure from present practice and reflect the present-day trend toward more liberal allowance for heat transfer, cable movement, and so forth, and along with the other elements of the system show orginality of a high order.

Herman Halperin (Commonwealth Edison Company, Chicago, Ill.): This paper is particularly thorough on matters relating to heating, cable ratings, and cable movement, and should materially aid others in designing cable systems.

As indicated by data in my 1939 and 1942 AIEE papers on load ratings of cable,[1,2] Mansfield's values are in some cases more conservative than mine. His heating constant for a six-duct conduit section is 1.48 degrees centigrade per watt per foot as compared to my value of 1.17, although his conduit has relatively much larger total cross section. Of course, his fine sand with a moisture content of around six per cent does not have good heat conductivity. I am wondering whether the lines have carried sufficient load to make feasible the procurement of heating test data at a number of locations on the route having two lines.

The author based his load-rating calculations on a dielectric power factor of 0.75 per cent, although factory tests gave him an average value of 0.4 per cent. Experimental operation of oil-filled cable at 132 kv for a net time of $8^1/_2$ years at temperatures up to and above 100 degrees centigrade, as well as other data, indicates, in my opinion, that it would be amply conservative to assume a power factor of 0.5 per cent. The basic ideas of design and installation of oil-filled cable lead to stability of the insulation in service. Based on the author's Figure 4, the use of 0.5 per cent would cause an increase of 17,000 kva in the normal rating of each circuit.

He presents very interesting information in Figures 3 and 4 on the effect of increasing insulation thickness on *increasing* the carrying capacity for two lines. It should be noted that this effect decreases as the soil heat conductivity improves to the more common values in the United States. Also, this effect decreases materially if the dielectric power factor is assumed as 0.45 or 0.5 per cent.

The author assumed a coefficient of friction of 0.5 for cable in a fiber duct, whereas Farnham and Titus in their 1942 AIEE paper on the Montreal 120-kv lines[3] give about one-half that value. It would be interesting

(*a*). To know results of Los Angeles measurements, made during the installation.

(*b*). To learn how measured cable movements line up with calculations.

In the Chicago system, the ducts are mostly of precast concrete, and for such ducts the coefficient is about 0.5. We found it necessary to make many measurements and to revise our conclusions on cable movement a few times before we were contented. Our dummy-manhole test data lend support to Mansfield's conclusion not to restrain the movement of joints, because such a procedure merely transfers the maximum sheath strain to near the duct mouth without reducing the strain.

I certainly agree with the author that it should be many decades before any sheath cracks develop in his cable. From only the standpoint of allowable cable movement, the maximum allowable span length between manholes could have been more than the selected limit of 710 feet. For an ordinary daily temperature range, my findings show that the cable movement does not increase for span lengths above 300 or 400 feet. These findings have been corroborated by experience with lengths up to 1,000 feet in Cincinnati and Montreal and by data in Schifreen's AIEE paper presented in June 1944.[4] If procedures such as the author outlined are used for installing and precleaning ducts, it becomes feasible to use extra-long lengths also from the standpoints of pulling forces and of sheath abrasion during installation.

Regarding sheath transients some data are given in the 1935 AIEE paper on transient voltage on bonded cable sheaths.[5] Some discharge in manholes due to transient sheath potentials incidental to switching have been noted in some cities for single-conductor lines having various schemes of bonding to eliminate sheath losses. In Chicago we have about 130 circuit-miles of such lines with an average service of about 12 years, and we have found that these transients and the discharges have been harmless, except for some small lots of equipment which were particularly and unusually subnormal. We have not found it necessary to install special electrodes connected between the sheaths, but they are inexpensive, and so this point is of no great consequence.

Usually where two lines are in parallel, it seems feasible to allow conductor temperatures much above the normal temperature limit of [illegible]0 degrees during an emergency with only one line in service. With an emergency temperature limit of 90 or 100 degrees, the permissible emergency loading would be roughly 190,000 kva.

Experience in Chicago, and elsewhere I believe, indicates that no particular risk is incurred nowadays in having two oil-filled single-conductor lines pass openly through common manholes. Fast relaying helps in having only small burning in case of a failure, and fireproofing over each exposed cable and associated joint protects each cable from external flame and mechanical damage.

This paper illustrates beautifully the multiplicity of problems arising in the design and operation of an underground high voltage line.

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G. B. Shanklin (General Electric Company, Schenectady, N. Y.): Mr. Mansfield deserves the greatest praise for the thorough way he has engineered this oil-filled-cable installation. His paper describes this in detail.

In my opinion, Mr. Mansfield has been rather conservative in assigning load-current ratings. His method results in a current rating of little more than 70 per cent of that obtained by the usual methods of calculation with standard duct constants. There are several reasons for this, and I would like to discuss these briefly.

In making these calculations Mr. Mansfield used a dielectric power factor of 0.75 per cent, which represents the guaranteed value. For oil-filled cable it is usual practice to assume a dielectric power factor of 0.5 per cent, since a value higher than this has never been measured on removed lengths of normal cable after long periods of service. This, alone, accounts for a very appreciable part of his reduction in carrying capacity. However, Figure 4 of his paper would not have shown such a wide variation in carrying capacity as a function of dielectric power factor if the other factors to be mentioned were not also rather conservative.

Over the usual range of soil moisture content Mr. Mansfield's Figure 2 agrees rather well with soil-resistivity measurements we have made in the past and is also representative of values used in the standard British formula for buried cable, when expressed in terms of net soil resistivity rather than absolute values. The net soil resistivity in the British formula is two thirds of the true absolute value and represents the difference between measurements *in situ* and in the laboratory. Mr. Mansfield designates his soil resistivity as "apparent" and obviously means that it represents the value derived from his formula 4. Expressed in these terms the general shape of his curve in Figure 2 agrees with previous data above a moisture content of 5 per cent. Below this, his apparent resistivity values are too high, and the curve bends up too sharply.

In making these calculations Mr. Mansfield assumed a thermal resistivity for concrete about three times as high as actual measurements we have seen. A representative thermal resistivity for concrete is 100 absolute, somewhat lower than the resistivity of surrounding soil, which is usually in the order of 180 absolute. In view of the fact that there is a three-inch shell of concrete around the duct bank and a heavy layer of concrete in the street pavement, the high value of resistivity Mr. Mansfield used accounts for some of the reduction in his current rating.

Formula 4 shows that Mr. Mansfield used a duct constant corresponding to the thermal resistance from loaded-duct wall to ambient earth. This represents a higher value than the standard duct constant, and there should be a correspondingly lower thermal resistance from cable-sheath surface to loaded-duct wall. Mr. Mansfield, however, used a sheath-surface resistivity of 1,200 which actually represents the thermal path from sheath surface to ambient air for a cable in free air and is the value used with standard duct constants. The true resistivity from sheath to loaded-duct wall is less than half this, depending upon cable diameter, and is usually in the order of 400 to 500.

Finally, Mr. Mansfield used a loss factor of 70 per cent for a daily load factor of 75 per cent. For this load factor the standard method assumes a loss factor of 62.5 per cent, as determined by analyzing a large number of typical daily load curves.

All of these things added together explain the conservative current rating Mr. Mansfield obtained. There is, of course, no objection to conservative current ratings for important power-cable lines. In fact, they are sometimes very sound practice. However, there has been a good deal of experience over a long period of years with current ratings based on standard methods, and they have been proved quite safe and well in line with actual copper temperatures measured.

Wm. A. Del Mar (Phelps Dodge Copper Products Corporation, Yonkers, N. Y.): Mr. Mansfield's paper shows an unusually careful study of a cable problem in its many aspects and will serve as a very useful reference to those who contemplate similar installations. The duct and manhole layouts are excellent, and it is refreshing to note the practical understanding of the sheath problem which has entered into their design.

There has been some confusion, I fear, in the carrying-capacity calculations but, if so, Mr. Mansfield has made no greater errors than have been made by others who have reason to be more deeply versed in this subject.

The formula for soil R_{th} may be exact, as Mr. Mansfield says, "for heat flow from a cylindrical conduit to the ground surface, if both are isothermals," but actually the formula is in error as it neglects the downward flow of heat to the deep isothermal region which is usually at a depth of about 50 feet where the temperature in most cities is about 15 degrees centigrade (59 degrees Fahrenheit) all the year round.

Mr. Mansfield seems to be under the impression that the ambient temperature to be used in his calculations, with that formula, is that of the ground around the cable, as he gives an ambient "maximum summer ground temperature of 25 degrees centigrade," which is evidently the temperature several feet below the sunbaked streets. The formula for soil, R_{th}, however, gives the resistance to the ground surface, and accordingly the pavement summer temperature must be used. This will be not less than 60 degrees centigrade (140 degrees Fahrenheit) for several hours daily in Los Angeles. Suppose the conductor to be at

Table II

Ratio of Diameters, Sheath OD/Duct ID	Correction Factor
0.0	1.0
0.1	0.95
0.2	0.88
0.3	0.80
0.4	0.75
0.5	0.63
0.6	0.55
0.7	0.40
0.73	0.37
0.8	0.28
0.9	0.15
1.0	0.00

70 degrees centigrade; then the thermal flow downward will be over 80 per cent of the total flow, and the thermal flow from conductor to its dual ambient, for a given copper temperature, will be over five times that calculated on the basis of upward heat flow alone.

Another error is in the equation for sheath R_{th}. This equation gives the thermal resistance to free air with no nearby surfaces. It does not apply to emission from a sheath to a duct surface an inch or so away. True, it is used in standard carrying-capacity calculations for cables in ducts but not in combination with a true thermal resistance from duct to ambient. It is there used in combination with a "duct constant" which has the physical dimensions of a thermal resistance but does not represent the actual thermal resistance from any one surface to any other. That is why it is called a "duct constant" and not the thermal resistance of a duct.

It appears that Mr. Mansfield's total R_{th} was made up as follows:

$$R_{th} = \text{paper } R_{th} + \text{sheath } R_{th} + 6(1.43 + \text{soil } R_{th})$$

If so, that is not correct, as his sheath R_{th} will be much too high. Table II of this discussion gives, to a fair degree of approximation, the correction factors that should be applied to his equation 3. In the present installation the cable–duct diameter ratio is 0.73 which gives a reduction factor of 0.37. If these two corrections are applied to the calculations the effect should be a very great increase in carrying capacity, but much of this is canceled by Mr. Mansfield's use of 25 degrees centigrade ground ambient temperature, as mentioned early in this discussion. However the net increase in carrying capacity is still very considerable and is my excuse for offering these criticisms.

C. T. Hatcher (Consolidated Edison Company of New York, Inc., New York, N. Y.): Mr. Mansfield has presented a very thorough discussion of the problems involved in planning, designing, and installing a 138-kv cable system. In the installation of single-conductor 138-kv oil-filled cable, a thorough consideration should be given to the conduit and manhole design, so that adequate clearances exist for the installation of the cable and for expansion of the cable under loading conditions. These items have been thoroughly considered by Mr. Mansfield, and it appears that the conduit and manhole system allows adequate space for the installation and operation of the cable.

Since the initial installations of oil-filled cable in 1927, both manufacturers and users have conducted tests for obtaining data in connection with reducing the insulation thickness of the oil-filled cables. These tests have resulted in allowable insulation-thickness reduction on all oil-filled cables, and in particular the 138-kv class has been reduced from 720 mils to 560 mils. Mr. Mansfield states that they selected 650 mils as the insulation thickness for the cable so as to obtain a substantial factor of safety against electrical failure and in addition attempts to justify the increased thickness on the basis of increased carrying capacity for the lines. The excellent operating record of oil-filled cables does not seem to justify the increased insulation thickness so as to obtain a factor of safety against electrical failure, and a study of the electrical characteristics of 138-kv oil-filled cable purchased by our company does not seem to justify the statement in reference to increased carrying capacity. In 1927, we installed approximately 35 miles of single-conductor, 600,000-circular-mil 138-kv cable insulated with 720 mils of paper. This cable was not shipped filled with oil but had to be evacuated and filled with oil in the field. After nine years of operation, a section of cable was removed and tested. The power factor at 70 degrees centigrade was found to be 0.543 per cent. During 1940, we purchased approximately 100,000 feet of single-conductor, 1,250,000-circular-mil, 138-kv cable with 560 mils of paper. The average power factor at 70 degrees centigrade, measured at the factory, was 0.388 per cent with a maximum of 0.475 per cent and a minimum of 0.270 per cent. With initial values such as these and the value of approximately 0.5 per cent after nine years of service, it appears reasonable to assume that the actual value of cable operating in service will not exceed 0.5 per cent. On this basis, it appears that the value of 0.75 per cent used by Mr. Mansfield is extremely pessimistic and that a value of 0.5 per cent or less should be used in computing the dielectric losses for the cable. If the 0.5 per cent value is applied to the curves in Figure 4 for a six per cent soil moisture, it will be noted that only a two per cent increase in capacity for 650 mils insulation is obtained. The value of 2 per cent would appear to be a maximum, and therefore for actual operating conditions it does not appear that there is any appreciable increase in the carrying capacity when various insulation thicknesses are considered for a given conductor size.

Mr. Mansfield states that insulating joints and sheath-bonding transformers were used to reduce sheath voltages and that it is a well-established fact that differences of more than 11 volts alternating current between cable sheaths and ground may cause corrosion under some conditions. We have been operating single-conductor cables at 27 kv, 45 kv, and 132 kv, where insulating joints have been used for reducing sheath voltages for a great number of years. The installations have been designed to operate anywhere from 12 volts to ground to 25 volts to ground. At one location, we have an operating condition of 12 volts to ground with a soil condition that is as bad as any we are aware of. A section of cable was removed from this location after approximately 16 years of operation and no indications of a-c electrolysis was found. It appears that cable can be operated at a higher value than the 11 volts to ground without being subjected to a-c electrolysis.

Carroll G. Mansfield: The discussions submitted in connection with this paper indicate an active interest in the subject of transmission-cable installation and raise some questions, particularly as to the load rating of the lines, which require further comment.

Several discussers have questioned the use of the guaranteed dielectric power factor of the cable rather than the actual measured value in the load calculations. This reflects chiefly a difference in emphasis on the two distinct purposes served by such calculations. Where the objective is simply to estimate the capacity of an existing cable of which the characteristics are fully known, the actual power factor, of course, would be used. But the purpose of the studies under discussion was to select a conductor size which would give an assured minimum capacity, with cable to be produced by a manufacturer then unknown. The analysis leading to conductor selection accordingly was based on the possibility that the cable obtained might have power factors approaching the guaranteed value, together with some lingering doubts as to power-factor stability over periods of time comparable with the life of the cable. The present capacity then was added as a modification when the actual dielectric power factors became known. The extra capacity so realized provides for a 50 per cent increase in the cable power factor, or for a substantial reduction in soil moisture during a series of dry years, or for some load growth beyond the minimum provided for.

In this connection, power-factor data such as those cited by Mr. Hatcher are encouraging but not, in themselves, conclusive. In view of the range of 0.270 to 0.475 per cent for new cable, the value of 0.543 per cent found for the single nine-year-old sample certainly does not preclude the possibility of there being other lengths in the older line having power factors of 0.75 per cent or even higher. The power factor of the older sample is 14 per cent and 100 per cent greater than the maximum and minimum for the new cable, and, while part of this no doubt is due to poorer technique of manufacture and installation, some may also represent an increase due to age. The accelerated tests mentioned by Mr. Halperin are somewhat more convincing, because of the higher temperatures and stresses involved, and, together with the available operating experience, justify considerable optimism as to power-factor stability in well-made cable.

Several discussers feel that the capacity given in the paper for a dielectric power factor of 0.40 per cent is still overly conservative and attribute this, in part, to the use of too high a value for the apparent thermal resistivity of the sheath surface, (including duct air), to which Mr. Shanklin adds the suggestion that too high a resistivity also may have been assumed for the concrete. Actually, the calculations involve no assumption as to the resistivity of concrete, nor do they admit of any piling up of errors in these two factors. This situation may be clarified by outlining in somewhat greater detail the procedure followed in establishing the resistances of these two portions of the thermal path. In the tests on the experimental conduit, temperatures were measured at the cable sheath and at the outer surface of the conduit structure. The resistance of the sheath surface and that through the fiber duct wall then were calculated, using $B = 1{,}200$ as the sheath-surface constant, and these were subtracted from the effective resistance between sheath and conduit surfaces derived from the test results. Next, the calculated resistance of the fiber wall and the remaining resistance of the concrete path were adjusted for the differences in duct diameter, fiber thickness, and length and cross section of the concrete paths, in the experimental and proposed conduits, and these corrected values then were combined into a single resistance representing the conduit structure. This use of the sheath-surface constant is very similar to that followed in the standard duct-constant method. The adjustments of fiber and concrete resistances are compensating to such an extent that a reduction in the value of B to 600 results in a negligible change in the total resistance between sheath and conduit surfaces. Hence if $B = 1{,}200$ is too high, when interpreted as resistivity from cable sheath to inner-duct wall, a transfer of a part of the temperature rise shown for "duct air" in Table I of the paper to the "conduit" item would be justified, but the total rise remains unchanged.

The table of correction factors quoted by Mr. Del Mar to be applied to the sheath constant B appears to be based on a very reasonable principle, but for a constant duct size they give higher values for small cables, which is directly contrary to the practice followed in using the Insulated Power Cable Engineers' Association duct-constant method which uses lower values for small cables. Obviously, closer agreement as to the meaning and value of this constant is required if inconsistent results are to be avoided.

In view of Mr. Shanklin's statement that "Formula 4 shows that the author used a duct constant corresponding to the thermal resistance from loaded-duct wall to ambient earth," it seems desirable to emphasize the fact that equation 4 applies only to the path from the conduit surface to earth. No formula is given for the resistance from duct wall to conduit surface, which is discussed in the paragraph following equation 4. Also his statement that the author used a loss factor of 70 per cent instead of the customary $62^1/_2$ per cent, may be misleading unless carefully interpreted. The loss factor used was $62^1/_2$ per cent, but this was not applied directly to the thermal resistances, since it was found in the experimental conduit tests that the percentages of constant-loss temperature rises actually attained in the soil and conduit were 70 and 75, respectively, when the loss factor was $62^1/_2$ per cent. This difference is usually neglected, but it is quite real, nevertheless.

Mr. Del Mar's criticism of the formula for soil R_{th} seems to imply the belief that a substantial part of the heat released in the conduit disappears into the interior of the earth, instead of ultimately finding its way to the surface. The author is unable to accept this view except in special cases, such as where the heat is carried away by moving ground water. The "deep isothermal region" is simply the region in which temperature gradients because of heat from the conduit become very small by reason of distance. This is precisely the condition required in the eccentric thermal field on which formula 4 is based and does not mean that heat leaving the conduit in a downward direction has been neglected. This formula is, however, derived for a stationary and homogeneous soil and, like all formulas, requires appropriate adjustment when actual conditions differ from those assumed in its derivation. Thus the effects of important heat absorbers, such as large storm drains or water mains or moving ground water, must be taken into account in analyzing any tests on conduits near which they are present. They should not be assumed in line-capacity calculations, however, unless known to be present at all points along the proposed line at which maximum cable temperatures otherwise would occur. More common effects are the variation of soil texture and moisture with depth and such minor factors as the noncylindrical shape of the conduit and temperature differences around its surface. In the present studies these were taken into account by deriving the effective soil resistivity from tests on an experimental conduit under conditions similar, in these respects, to those on the proposed line. This compensation is not perfect but is probably sufficiently accurate, and no better method is yet available.

If the time required for heat to pass from the cable to the ground surface were short compared with the period of daily maximum pavement temperature, Mr. Del Mar's suggestion that this maximum pavement temperature be used as the ambient temperature in calculations with formula 4 would be correct. Fortunately, because of the relatively large heat capacity of the soil, this time is long in comparison with the daily cycle, and the effective ambient temperature is the average surface temperature over a period of time roughly equal to the average time required for heat to reach the surface along its many paths of varying length. This is well represented by the ground temperature at a depth at which the daily fluctuation becomes negligible.

In a further effort to identify the source of the differences in estimates of line capacity, the standard IPCEA duct-constant method was used to calculate the sheath temperature for a constant loss of 4.08 watts per foot of cable in the experimental conduit, from which data used in calculations for the 138-kv line were obtained. The calculated sheath-temperature rise over earth ambient temperature is 44 degrees centigrade. The observed rise was 13 per cent greater than this in the corner ducts and 25 per cent greater in noncorner ducts. This discrepancy would seem to indicate quite clearly that the difference in load estimates represents a real difference in conditions rather than an error in calculation.

Loads on the present lines have not yet reached values which would make comparisons of calculated and actual temperatures and cable movements of practical value.

The coefficient of friction of 0.5 used in the cable-movement calculations was based on the similar value previously given by Mr. Halperin and on a few measurements of our own, most of which were, however, made on groups of single-conductor cables. Tests made during installation of the 138-kv cable, which was well greased, gave values close to the 0.25 reported by Mr. Farnham, but it is expected that this will increase somewhat as the grease dries out or is washed away. If it does not, the provisions made for additional movement are ample.

The successful use by Mr. Hatcher and his associates of alternating sheath voltages approximately double those which Mr. Halperin finds to represent the safe limit are of course well known, but the reasons for this difference are still obscure. In the present installation this did not prove to be a critical factor. The span length was determined principally by other considerations, and it was felt that the relatively small saving which could be made by omitting half of the bonding transformers did not justify the gamble that corrosion conditions here would resemble those in New York more closely than those in Chicago.

In reference to the series of questions raised by Mr. Farnham: No data are available from which a direct comparison of actual costs of the conduit used on these lines and a conventional rectangular conduit of similar size can be made. The labor of placing the ducts is not increased, and the volume of concrete required is only 10 or 12 per cent greater than in a rectangular conduit with the same phase and circuit spacings. Mr. Farnham gives added rigidity as one of the benefits resulting from the use of fireproofing on the cable bends. We considered this feature as likely to be definitely harmful because of the tendency of the covering to crack at the points of maximum stress and so add to the concentration of sheath strain at that point.

It is interesting to hear that audible vibration of the cables has been observed elsewhere. While the segmental conductor used in the Montreal cable is undoubtedly more solid than the usual stranded type, it is not impossible that even these segments may have sufficient freedom at some points to permit the very small movements required by this vibration. The theory that this vibration originates in the steel core is handicapped by the lack of any means by which the exciting force might be applied, particularly since this force seems to be of electrostatic origin.

We have had no occasion to locate reservoir cell leaks as yet. Our conclusion from a study of the reservoir action, and confirmed by the manufacturer, is that such a leak would result in a small abnormal rise in the immersion oil level. The effect is subject to some qualifications, however, and, accurate diagnosis undoubtedly will require that the reservoir be disconnected from the system and tested.

Solderless Terminals

Discussion and authors' closure of paper 44-220 by F. H. Wells and J. C. Balsbaugh, presented at the AIEE Los Angeles technical meeting, Los Angeles, Calif., August 29–September 1, 1944, and published in AIEE TRANSACTIONS, 1944, December section, pages 933–8.

Donald L. Moran (nonmember; lieutenant, Air Technical Service Command, Wright Field, Dayton, Ohio): Wells and Balsbaugh present the background of the need for and a description of tests conducted on a crimp-type solderless terminal.

This is the only type solderless terminal standardized by the Army Air Forces at the present time and is covered by drawing *AN*659. This drawing includes provisions for each size of wire specified in specification *AN-J-C*-48 with two stud sizes for each wire size. The Army Air Forces at the present time are standardizing a solderless terminal which will incorporate a cast-copper body and barrel with threads on the inside of the barrel. A machined brass jam nut is inserted in the barrel and forces a two-piece tapered slotted sleeve serrated on the inside diameter to hold the wire in position. These will be standardized for American Wire Gauge wire sizes 2 to 0000 inclusive. Another type of solderless terminal being standardized by the Army Air Forces incorporates a copper stamped tongue and body, a brass nut, and a slotted brass sleeve which is tapered on the tongue end and threaded on the opposite end for application of the brass nut. The sleeve is inserted on the tongue end of the terminal, and the tightening of the brass nut on the sleeve clamps the wire firmly in the terminal. This type terminal covers American Wire Gauge sizes 2, 0, and 00. Tin plating protects this type of terminal from excessive corrosion. These two new types of terminals are for high-current circuits and include the advantage that no special tools are required for application.

Many aircraft manufacturers complain that their workers cannot satisfactorily apply the solderless-type terminal, because some of the wires may be cut when the insulation is removed. This is true particularly for small-size wires. The manufacturers who have experienced employees still prefer to use the soldered terminal to be sure of satisfactory application or use the solderless terminal reinforced with a drop of solder. This gives a more dependable terminal performance although the time of application is appreciably increased.

The paper failed to mention whether these solderless terminals were applied with a press using an appropriate crimping die, or the hand crimping tools similar to those used by the services. This would make an appreciable difference in the results of tests. Because of the uniformity of test results, it could be assumed that the crimping press was used for application of the terminals. Tests by the Army Air Forces of solderless terminals using a hand crimping tool show an irregular appreciably higher resistance for the terminal for similar current flow on the same-type terminals.

The test conditions cited by Wells and Balsbaugh are similar to those conducted by the Army Air Forces. It is believed that the corrosion tests should have included a higher percentage of sodium-chloride spray. A 20 per cent solution as specified in specification *AN-QQ-S*-91 seems to be satisfactory to assimilate accelerated conditions encountered in salt-water areas. A humidity–temperature cycling test with relative humidity from 95 to 100 per cent would increase the extent of corrosion, since "breathing" of the metal would result.

In general, however, the test conditions and results seem satisfactory to ascertain the correctness of the conclusion reached by Wells and Balsbaugh. Failure to take into consideration the new types of solderless connectors which will be used by the Services, the methods of application of the solderless terminals not being clear, and test conditions being slightly different from accelerated service conditions will not affect appreciably the results and conclusions reached in this technical paper.

F. H. Wells and J. C. Balsbaugh: We have been very glad to obtain the comments of Lieutenant Moran, both because of his experience with the use of solderless connectors by the services in actual application, and also because of his familiarity with the test procedures and results of the service laboratories.

With regard to the question of whether the solderless terminals on which the tests reported were made were prepared by hand crimping tools or by a press with crimping die, all terminals were prepared using a hand tool, with the exception of the test results reported in Table I of the paper. These were on an assembly of 500 connectors in series, as shown in Figures 3 and 4. The hand crimping was done by a number of laboratory technicians. Admittedly, in the preparation of these crimped terminals care is taken to be reasonably certain that full crimping is obtained. In general, from the laboratory point of view, we have thought that hand crimping will give quite consistent results.

With regard to the question of percentage of sodium chloride in the spray used in the tests, at present we are making a very complete series of tests in which the percentage of sodium chloride is varied from four per cent through intermediate steps to a maximum of 25 per cent (saturated solution). While these tests are at present not complete, the initial results indicate quite definitely that the lower percentage of sodium chloride, namely four per cent, gives the most severe corrosion conditions. These results may seem to be somewhat anomalous and quite unexpected. Two reasons may be given for these results: first, the use of the higher salt concentration gives salt deposits which may decrease, to a certain extent, the entrance of condensate into the terminal; second, and probably more important, oxygen concentration in the solution and in the spray decreases with the increasing salt concentration. The latter is very important from the point of view of corrosion.

Altitude Rating of Electric Apparatus

Discussion and author's closure of paper 44-228 by Paul Lebenbaum, Jr., presented at the AIEE Los Angeles technical meeting, Los Angeles, Calif., August 29–September 1, 1944, and published in AIEE TRANSACTIONS, 1944, December section, pages 955–60.

Harvey J. Finison (Wright Field, Dayton, Ohio): Much of the work on which Mr. Lebenbaum's paper is based was carried out in 1941 and 1942. At that time much of the experience in high-output machines for high-altitude operation was still in a formative stage. An attempt had been made to apply type *P*-1 200-ampere generators fan-cooled and derated to 125 amperes at sea level on high-altitude aircraft. The results were disastrous. Mr. Lebenbaum's work has been very valuable in formulating decisions on methods of cooling which should be used for particular applications.

The installation of numerous high-altitude chambers has made the actual calculation of an altitude rating less desirable than its determination by tests. However, a knowledge of the basic principles of machine cooling as given in Mr. Lebenbaum's paper is essential to the understanding of those test results and in the design and application of a machine.

There are a number of cases where self-cooled machines designed for low altitude are still applied on high-altitude aircraft. In most cases the load on the machine is sufficiently less than machine rating that immediate failure does not occur, but the service life is considerably shorter than it should be.

Mr. Lebenbaum has demonstrated that for high-altitude operation forced cooling from the slip stream is far more effective than self-cooling by fan. Of course, fan-cooled machines can be designed which will cool adequately at altitude, but the weight is considerably above an equivalent forced-cooled machine. Forced cooling can be obtained only at the expense of airplane drag. Where the weight of the machine is not limited by other considerations, as is the case with main-engine mounted generators, the choice between forced and self-cooling must be based on relative effect of increased weight against increased drag.

There is considerable need for the establishing of some principles that may be followed in evaluating the relative economics of forced and self-cooled machines.

E. R. Summers (General Electric Company, Schenectady, N. Y.): Mr. Lebenbaum's paper is based on work done three or four years ago before suitable high-altitude chambers were available for testing pressure-ventilated aircraft generators. His procedure for calculating high-altitude performance from sea-level tests was of great help in designing the earlier generators.

Several of nature's laws have been unfavorable to electric apparatus at high altitudes. But one physical relation has been in our favor; namely, that the mass flow of air through a blast-ventilated generator decreases only as the square root of the air density. When the type *P*-1 six-kilowatt aircraft generators were initially designed, a minimum ventilating-air pressure of about ten inches of water was expected. However, the rammed air tubes had many bends and joints, and in some cases only four to six inches of net pressure were available, instead of ten. These variations in service conditions were large compared to the exactness of the formulas and tests from which the generators were designed. The square-root relation was helpful, because when the available pressure was reduced 50 per cent, the mass flow of air was $\sqrt{0.5}$ or 71 per cent of the full expected value.

Design calculations were based also on only a few degrees increase in air temperature by adiabatic compression in the duct leading to the generator. However, this two-inch-diameter duct passed between the engine cylinders which, together with other losses, caused the air to increase as much as 30 degrees centigrade (from −55 to −25 degrees centigrade) in some cases before it reached the generators. This reduced flow of preheated ventilating air was disturbing to generator designers who had little margin in temperature rise for overloads or for erratic behavior of carbon brushes. Further attention should be given by aircraft designers to the more efficient delivery of colder air at higher pressure to the generators.

In an earlier discussion at this meeting, Lieutenant Colonel T. B. Holliday emphasized that the weight advantage of a blast-cooled generator was sometimes largely offset by the extra fuel required to drag the ventilating duct. The increased weight of engine wall casting and of mounting flange also must be considered if an overhung generator were made large enough for self-ventilation.

The importance of natural convection and radiation should not be overlooked when pressure-ventilated machines are being tested. I recently saw a high-altitude laboratory where *P*-1 generators were tested with −50-degree-centigrade ambient temperature around outside of stator frame. Actual aircraft-engine cell conditions may be +50-degrees centigrade or more. This error of 100 degrees centigrade in ambient temperature increased radiation and free convection losses by an amount corresponding to 20 per cent of total generator losses, and the hot-spot temperature rise of windings would be about 20 per cent too low in such a test. This discrepancy might be very significant in a closely designed machine.

Mr. Lebenbaum states that axial velocity of air flow is more than twice the peripheral velocity of rotor on pressure-ventilated generators. This is true for the earlier low-speed machines, but not for the more recently designed *R*-1 nine-kilowatt generators which have a top speed of 8,000 instead of 4,400 rpm.

Since appropriate altitude chambers are now available for testing all sizes of aircraft generators at any simulated altitude or temperature condition, the present trend is to obtain high-altitude performance data directly from chamber tests, rather than to make temperature calculations based on sea-level tests only. Such chamber tests are an over-all check of all machine parts.

The question was raised recently as to whether the shaft would be twisted off if a generator were started suddenly when the grease in the bearings was solidified at −60 degrees centigrade. The altitude chamber offers a quick means of checking special conditions which cannot be calculated from conventional sea-level tests.

Paul Lebenbaum, Jr.: The author wishes to thank Mr. Finison for his comments on the application of the theory presented in this paper.

Mr. Summers has mentioned that actual temperatures and pressure heads of the cooling air at the entrance to the generator have been found to be quite different from theoretical values. This fact was recognized as a possibility in the paper, but the assumption was made, on the basis of data available at the time of writing, that the temperature rise and pressure loss were negligible. As Mr. Summers shows, this assumption no longer can be made, and the temperature of the cooling air, θa_1, should be taken as the actual temperature of the cooling air at the entrance to the generator and not that of the air external to the airplane. The pressure drop in the duct work leading to the generator does not enter directly into the equations, but it does effect the weight flow of the cooling air, *W*. This fact shows that in altitude-chamber or sea-level testing of generators that the duct work or its equivalent actually used on the installation should be part of the test equipment. In any case, the weight flow of cooling air through a blast-cooled generator still varies as the square root of the ramming head pressure, as Mr. Summers has pointed out.

Mr. Summers also has brought out the fact that on newer higher-speed machines the definition of a separately or self-ventilated generator based on the ratio of axial velocity of the cooling air to the peripheral velocity of the rotor no longer holds. This statement is correct, and further investigation is indicated. For the present it is suggested that equation 8 of the paper still be used for self-ventilated and equation 9 for separately ventilated generators.

Aircraft-Electric-Accessory-Vibration Investigations

Discussion of paper 44-194 by D. R. Miller, presented at the AIEE Los Angeles technical meeting, Los Angeles, Calif., August 29–September 1, 1944, and published in AIEE TRANSACTIONS, 1944, pages 1283–90.

R. A. Cole (nonmember) and **Wayne D. Cannon** (Wright Aeronautical Corporation, Paterson, N. J.): In presenting the vibration problem to designers of electric equipment for aircraft, Mr. Miller has made a needed contribution.

Unfortunately, the problem has not been well understood, and only a few designs have been planned from their inception to endure the maximum conditions found in normal aircraft service.

Size and weight considerations for aircraft equipment are so stringent that structural strength of designs requires the most careful attention.

We applaud Mr. Miller's efforts to determine by analysis and laboratory means the suitability of a generator design for a specific application. This method of test is very much more economical than full-scale engine tests or flight tests. However, we feel that, until laboratory results have been verified by considerable service results, the sound approach will be physical tests of generators on engines for extended runs and this work verified by flight test.

Drawing from our experience of aircraft-engine-vibration studies, we offer the following comments to Mr. Miller's paper in the hope that some additional light can be focused on the problem.

In designing for a specific natural frequency or range of frequencies, consideration must be given to the vibration characteristics of the engine on which the generator is mounted. All large modern engines are supported by rubber springs which are calculated to give a low natural frequency of the power plant. This method permits the mass of the power plant to absorb the vibratory forces without transmitting the disturbance into the airplane structure. For effective isolation, as Mr. Miller points out, the ratio of disturbing frequency to natural frequency must be greater than 1.4, so the engine mount is usually designed for a natural frequency between the idle and warm-up ranges, say 500 to 900 cycles per minute. The engine orders which are always present in a properly functioning engine are at crankshaft rpm and above, so that there is no danger of resonance in the usual operating range of 1,200 to 3,000 rpm. Propeller orders and half-order combustion disturbances may excite mount resonance in the lower end of the range, particularly on engines with low-reduction gear ratios, or under conditions of severe propeller unbalance. The designer of the generator must assume that these conditions will occur sometime, even though they may not fall within the normal operating rpm range. For example, the engine may be damaged in a combat area and left windmilling in order to deceive the enemy; the resulting engine speed usually will fall between 1,000 and 1,400 rpm, which is low enough for propeller order to excite mount resonance.

Under mount-resonance conditions, engine amplitudes will be very large, being limited usually by snubbing provisions designed in the rubber springs. The accelerations however, will be low because the frequency is low (±0.1 inch amplitude at 600 cycles per minute or ten cycles per second gives an acceleration of 1 g). However, if the generator or any of its parts has a natural frequency about the same as that of the power plant on its mount, trouble must be expected. Resonant vibration of the engine crankcase structure also may occur, but it usually is damped by sufficient friction at the bolted surfaces.

We believe that any empirical figure or rule of thumb for natural frequency must be used with great caution, since a change in engine conditions may put the design in a resonance range. For instance, Mr. Miller recommends 150 cycles per second (9,000 cycles per minute) as the minimum flexural natural frequency for a generator mounting. This figure is satisfactory to avoid the $2^1/_2$-order whirl resonance which he reports on the 14-cylinder two-row radial engine used on the *B*-24 airplane. However, in the 18-cylinder engines which now are used on many of our combat aircraft, the corresponding order is $3^1/_2$, and, if this engine whirl peaks above 2,550 rpm, resonant vibration will occur on a 150-cycle-per-second generator mounting. For 2,800-rpm take-off speed, which is now in service use on 18-cylinder engines, the mounting frequency would have to be at least 175 cycles per second (10,500 cycles per minute) to clear a $3^1/_2$ order at take-off. The possibility of higher-frequency whirls must not be overlooked. These modes may occur at an order equal to ±1 plus any multiple of firing frequency of one bank. They are usually small and cause trouble only when they excite an underdamped resonance.

Mr. Miller's curves permit some interesting observations on the vibration of the power plant and its effect on generators and other accessories mounted on the engine. It is reasonable to assume that the maximum stress in the mounting flange will be some direct function of the vibratory acceleration. Figure 11 of the paper shows that the maximum vibratory displacement on the generator of a *B*-17*F* airplane was ± 0.030 inch, with an acceleration of about 8 g. The maximum acceleration in the same direction was about 50 g, or six times the first acceleration, with only $^1/_{10}$ as much displacement—about ±0.0015 inch. This condition occurs, because the acceleration varies directly as the first power of the displacement and as the square of the frequency, and the frequency ratio is about 12 to 1. The example points out the inadequacy of limits on vibratory displacement without regard to frequency or acceleration.

Another interesting point in Figure 11 is the relatively large longitudinal or thrustwise vibration, which reaches about ±0.015 inch at 60 cycles per second (3,600 cycles per minute), and ±0.00055 inch at 650 cycles per second (39,000 cycles per minute). The latter figure corresponds to an acceleration of nearly 25 g, which is quite severe. Any thrustwise vibration arouses interest, because there are no thrustwise disturbing forces in the engine except for components of lateral or vertical forces resulting in certain deflections. It is possible that the motion of the generator shows a thrustwise component of a whirling mode, since the generator is mounted below the thrustwise or crankshaft axis. The resonant frequencies for both peaks are higher than those of the corresponding lateral peaks, indicating that the thrustwise resonance on the generator is not the same whirl as the vertical and lateral but involves a definite thrustwise mode.

For engine-vibration work, we prefer to express frequencies in cycles per minute, so that orders are obvious when engine speeds are given. This information is usually necessary for a full understanding of the cause of the vibration and often points directly to a possible solution. It is also very desirable to indicate whether displacements are expressed as "plus and minus" or "double amplitude." Both of these terms are self-explanatory; they should not be confused with "total amplitude" which is taken to mean the amplitude of a complex wave as distinguished from the amplitudes of its harmonic components.

C. H. Havill (nonmember; Bendix Aviation Corporation, Teterboro, N. J.): This paper contains data on aircraft generators both under translational and torsional vibration. The translational vibrations for various engine and propeller combinations were discussed quite thoroughly in "Aircraft-Engine-Accessory Vibration," by John Tyler of Pratt and Whitney Aircraft,[1] and hence, need no further comments on my part except to restate that the overlapping frequency spectrums of the various engines have driven the accessory manufacturer to use as stiff a construction as possible, especially for the flanges; but in spite of this, the 2.5 order and 4.5 order usually are in resonance during some part of the useful range of engine speeds. Further stiffening of the engine rear end casting may help raise the period. However, the time is fast approaching when the accessories will have to be removed from the engine to avoid flange failures or stud failures. This was discussed in my comments on Mr. Tyler's well-written paper.

As to torsional vibrations as discussed in Mr. Miller's paper, I agree that a torsional dampener is necessary at present in many cases and desirable in all cases. However, my criticism is that Mr. Miller describes a dampener that does not go far enough. I believe that, if a dampener is used, it should act at an appreciable radius—not on just the radius of the quill which is small. The dampener should cause a dampening factor of at least 0.4 to 0.5, so that the generator can be used on small auxiliary engines, in line engines, tandem in-line engines, as well as radial engines. For light weight and ease of manufacture, we have developed a dampener that has been tested over this wide range. It consists of rubbing plates with asbestos brake lining between. With this arrangement, life tests at resonance can be run and the maximum deflections of the quill kept well within the fatigue strength.

I believe that if all accessories were driven by a properly designed quill and properly designed dampener, all quill failures would disappear. Incorporating a vibration dampener in all generators would involve overprotection in many cases, but would permit universal application of generators on any engine, whether it be a conventional radial type, tandem in line, or small auxiliary engine. Sooner or later, some of these generators would appear installed on small auxiliary engines or tandem in-line engines where a dampener was needed. Eventually I hope that the dampener will be used on all installations, which is what Mr. Miller has stated is desired.

REFERENCE

1. AIRCRAFT-ENGINE-ACCESSORY VIBRATION, John M. Tyler. AIEE TRANSACTIONS, volume 63, 1944, pages 1334–49.

Aircraft-Engine-Accessory Vibration

Discussion and author's closure of paper 44-227 by John Tyler, presented at the AIEE Los Angeles technical meeting, Los Angeles, Calif., August 29–September 1, 1944, and published in AIEE TRANSACTIONS, 1944, pages 1334–49.

E. A. Boniface (nonmember; American Airlines, LaGuardia Field, New York, N. Y.): There is no denying that the compilation of vibration spectra for the various engine–propeller–accessory combinations will provide for more accurate predictions of the suitability of accessories from a service-life standpoint as affected by periods and amplitudes of vibration. This of course will assume greater significance with the increased-horsepower engines under development with their proportionally larger accessories. It would, therefore, appear that this type of investigation should be considered by the industry as a necessary addition to the engine-installation specifications.

As air-line operators we have had our share of unpleasant associations with the problems of fatigue of "overhung masses" in the engine rear-section location, and unfortunately it has sometimes been found in investigation that the stresses set up at the fracture points have been well within the fatigue limits, and consequently there appeared to be no logical explanation for the failures. Since the failures were chronic, it was not possible to charge them off to isolated cases of faulty material. This has led to the thought that possibly additional consideration should be given to amplitudes of vibration experienced during starting, especially under winter conditions with heavy backfiring. The vibration period under these conditions is low, but it is conceivable that the amplitudes may be sufficient to create a fracture nucleus that could develop subsequently into an ultimate failure, even with moderate vibrational stresses present. We should be very interested in receiving Mr. Tyler's comments on this phase of the problem.

Mr. Tyler's point that charts such as these be used by the accessory manufacturer certainly should not be considered as a suggestion, but more as a "must," if we are to make any logical engineering advancements from the present hit-and-miss method of accessory selection. Too often, development research, such as presented in this paper, points out the need for collaboration on the part of the related manufacturers, but fails in its purpose because of lack of follow-up.

C. H. Havill (nonmember; Bendix Aviation Corporation, Teterboro, N. J.): The paper, "Aircraft-Engine-Accessory Vibration," by John Tyler has placed all in one place a large amount of statistical data concerning the translational vibrations of the various engine accessories mounted on the rear of the engine. These data are extremely valuable to Eclipse-Pioneer division of the Bendix Corporation in their design of accessories. Unfortunately, these data pertain only to a few types of radial aircraft engines and do not take into account some of the problems that the accessory manufacturer meets when considering that his product must "live" on radial engines of other manufactures as well as on in-line engines. During this war the tendency has been to expect an accessory to be satisfactory on nearly all engine–propeller combinations. As engines are getting larger and natural frequencies are rising, it is getting more and more difficult for a given accessory to "live" on all combinations. Mr. Tyler has creditably pointed this out.

There is a tendency for nearly everyone to assume that the accessory flange on the engine studs fail only under resonance conditions and to neglect the high level of stresses that are produced by the higher harmonics of the various sources of frequencies present. It is true that, when pure resonance occurs, parts fall off in short order, and it is also true that resonance stresses are in addition to the stresses produced by the accelerations caused by the combinations of the several frequencies whose vector sums reach high values for a microscopic part of a second and whose resultant amplitudes are extremely small; yet they cause high-frequency cracks in steel flanges of accessories and do other damage.

For the remedies suggested by Mr. Tyler I believe everyone will agree that the best one is to get the secondary accessories (those accessories that are not needed to run the engine) off the engine. However, until that evolution has been accomplished, the strengthening up of the engine rear section (raising its period) might be of some help in a few borderline cases. With the present Society of Automotive Engineers flanges the natural periods of the accessory units cannot be raised very much as the present stiffness of a heavy forged steel flange is about as far as practical design can go, since natural frequencies change directly as the square root of the stiffness.

The days of having a calculated minimum natural period of 9,000 cycles per minute (which is reduced to about 8,000 cycles per minute when mounted on the engine because of the flexibility of the engine casting) and designing for a primary stress of 60 g, are over, and there seems to be no good solution except to remove the accessories from the engine.

Mr. Tyler has pointed out that the vibratory stresses in the propeller and in the accessory flanges are about the same order of magnitude which has been realized by us for sometime and probably dates back to Newton in that every action has an equal and opposite reaction.

For those who have not put much thought on this subject, the "whirl" that Mr. Tyler speaks of is a polar vibration where any point on the engine (not at the node) describes a circle or elipse with a wavy perimeter. This can be visualized as a number of vectors of various lengths rotating about a point, and these vectors rotate at different speeds representing the various orders of vibration. The sudden changes in the distance from the center to the erratic wavy perimeter give rise to very high shear loads at various points in the accessory mounting.

The construction of a single-frequency testing machine with nonrotating parts that simulates one revolving vector is a definite advance in the art. Of course, intelligent testing using the frequency spectrum of the propeller–engine combination must be used; otherwise everything can be broken up or nothing broken. This machine will eliminate the primary calculations for natural periods, but, being only a single-frequency machine, it cannot give all the rotating vectors at one time. However, since the means are now available for every accessory manufacturer to measure the primary frequencies, it is well that they should—however, the fact that these come within certain specifications as tested at single frequencies will not guarantee no failures in flight but will help remove the failures due to primary resonance.

In summary: this paper by Mr. Tyler is an excellent presentation of a broad subject and points out that, even though good selection of propeller–engine combinations with selected accessories may provide solutions for some of the immediate problems, the time is fast approaching when the overlapping spectrums of all engines will require higher natural frequencies than it is possible to attain in the accessories themselves. The only permanent solution is to remove the secondary accessories from the engine frame, and the sooner this is done the better. A radical change in engine design is necessary if there is to be a net saving in installation weight.

D. R. Miller (General Electric Company, Schenectady, N. Y.): Mr. Tyler's paper goes a long way in fulfilling the need for information in the hands of design engineers on the characteristics of the vibration to which aircraft accessories are subjected. The solution of the accessory-vibration problem would be aided further if similar papers were written by other investigators in the field of aircraft vibration. The matter of torsional vibration to which aircraft generators are subjected is particularly of immediate interest. Information also is needed on the characteristics of vibration to which other than engine-mounted accessories are subjected. Present specifications call for vibration tests up to 50–60 cycles per second, but much higher vibration frequencies are present in aircraft-accessory installations.

It may be possible to reduce failures of engine-mounted accessories by reducing the vibration on the accessory pad by improving propeller-damping characteristics. It would be interesting to consider the damping of propeller vibration which might be afforded by partially filling hollow propeller blades with fluid or powdered metal or by using some other friction device. There is also the possibility of reducing the vibration magnification of the generator at resonance in flexure by the use of a damping device.

Because of the extent of failures which have been encountered on installations of accessories on the engines of military aircraft, it seems highly desirable that new designs of the conventional gasoline engine for aircraft include an accessory gearbox to be installed on the aircraft structure as suggested by Mr. Tyler. Such a gearbox should have a means for isolation of the torsional vibration of the engine so as to eliminate the need for complicated flexible-drive couplings and damping devices on all of the accessories.

John Tyler: Several worth-while comments and suggestions have been offered in the discussions on this paper.

D. R. Miller points out that torsional-vibration problems are becoming increasingly important and that relatively few data are now available on this subject. This is true, but it must be recognized that the considerable data on this subject which are presented in this paper on bending vibrations have been collected over a period of several years. When we have accumulated several years of experience on torsional-vibration problems, we expect to be in a position to draw similar charts for the aircraft power-plant torsional-vibration system.

Mr. Miller also suggests that the use of damping to the power-plant vibration system should help in reducing accessory vibrations. It is suggested that probably attempts to increase the damping in the accessory mounting would be much more fruitful than attempts to increase the damping of the propeller-blade vibration.

E. A. Boniface points out that unexplainable failures of aircraft-engine accessories may be the result of large amplitudes of vibration occurring during starting or backfiring, or as a result of some other unusually large excitation. While the author agrees that this may be the case in some instances, it is believed more likely that these unexplainable failures are due to high stresses resulting from unknown vibrations at the natural frequencies of the accessories. The reason for this opinion is that the *g*'s involved in the starting, backfiring, and similar conditions are on the order of 1 to 5, while the *g*'s resulting from vibration at accessory resonance are on the order of 10 to 100.

As Doctor Havill points out, more than one frequency of vibration may be involved in accessory failures. Therefore, while the Pratt and Whitney accessory-vibration-testing machine as used to date duplicates the normal condition where large vibration stresses are produced as a result of excitation at a single natural frequency of the system, it may be desirable to excite simultaneously the machine at large amplitude at more than one frequency to duplicate similar conditions occasionally encountered in service.

In closing, the author would like to point out that the data on aircraft-engine vibration presented may be used for many purposes beyond accessory-vibration problems. By converting the whirl amplitudes on the accessory-mounting pads presented in the charts in this paper to whirl amplitudes at other points on the engine (assuming the whirl to have a node at the engine center of gravity), it is possible to analyze the vibration characteristics of cowls, exhaust collectors, short stacks, or any other accessories attached to the engine.

High-Altitude Brush Problem

Discussion and author's closure of paper 44-208 by D. Ramadanoff and S. W. Glass, presented at the AIEE Los Angeles technical meeting, Los Angeles, Calif., August 29–September 1, 1944, and published in AIEE TRANSACTIONS, 1944, November section, pages 825–30.

E. R. Summers (General Electric Company, Schenectady, N. Y.): This paper represents a careful attempt to learn more about carbon brushes. Electric-apparatus manufacturers welcome such work, because more fundamental information is needed.

Their data for brush friction were obtained without current flowing. Friction varies widely on most brushes as the current density is varied. In many cases, the brush friction and contact drop change almost constantly, sometimes by 300 to 500 per cent, even though test conditions appear constant. The specific values of coefficient of friction and also the plotted curves in this paper imply an exactness and consistency of brush performance which do not exist.

It is stated that adsorbed moisture is easily driven off when a collector ring starts to turn in an evacuated chamber. The film may be stripped off the ring surface quickly, but adsorbed moisture is not so easily driven out of the pores of a brush.

The altitude chamber used for machine tests has a volume of about two cubic feet. With ten changes of air per hour, about 20 cubic feet of fresh low-pressure air are admitted each hour. The self-ventilated gun-turret Amplidynes take in 1,200 cubic feet of air per hour. This means that the same air passes through an Amplidyne 1,200/20 or 60 times on the average before being pumped out of the chamber described. There is some chance for accumulation of contamination, but recirculation of air also occurs to some extent with self-ventilated apparatus in actual service on aircraft.

The dew point of air entering the test chamber is stated to be −70 degrees centigrade. What is the dew point as it leaves the chamber? How much leakage is there of outside air which may carry several thousand times as much moisture per cubic foot? The lack of visible frost accumulation on the −70-degree-centigrade chamber walls, does not prove necessarily that all of the air entering chamber has this low dew point.

Suppose that air entered chamber at −40-degrees centigrade dew point, and moisture were condensed out to −70-degrees centigrade dew point. This corresponds to 0.05 grain condensation per cubic foot of air, or to only one grain per hour with 20 cubic foot of air per hour. It would take 7,000 hours, or almost a year of continuous operation, to condense out one pound of water. The condensate in a one-day test with this 30-degree-centigrade drop in dew point would be only one cubic centimeter, which would hardly be noticed on the relatively large inner surface area of the chamber. Yet such small amounts of water are significant in brush tests.

The −70 degrees centigrade on chamber walls by refrigeration is good assurance that low dew point will be maintained within the chamber even though the chemical air dryer occasionally may operate at higher dew points.

If the alumina dryer does deliver air at −70 degrees centigrade dew point, the dew point would drop about 15 degrees centigrade lower if the air is expanded thereafter from sea-level pressure to 35,000 feet altitude conditions within the test chamber.

W. E. Campbell and G. T. Kohman (nonmembers; Bell Telephone Laboratories, Inc., New York, N. Y.): The authors are to be congratulated on their comprehensive attack on the problem of carbon-brush wear. Their findings on the effects of different gases on wear reduction are in good general agreement with those obtained in the Bell Telephone Laboratories, which used flowing mixtures at atmospheric pressure of a given gas with nitrogen. There are, however, differences which merit some discussion.

In our studies, dry carbon dioxide was found to stop dusting at a concentration corresponding to a partial pressure of 38 centimeters of mercury, whereas the authors state that brushes dust in dry carbon dioxide. Possibly their studies were carried out at partial pressures lower than 38 centimeters.

In our studies of the effect of oxygen, dusting was not stopped until a concentration corresponding to a limiting partial pressure of 25 centimeters was reached, whereas in the present studies the limiting oxygen pressure was much lower—varying from 1.1 to 9.7 centimeters of mercury for brush temperatures ranging from 40 to 130 degrees centigrade. The origin of the condensed water vapor on the commutator surface during their tests with oxygen is not clear to us from the authors' explanation. Presumably enough water was present in the oxygen used to affect the limiting pressure at different temperatures. The higher limiting pressure we obtained might be due to a lower water content of the oxygen used in our studies.

W. T. Breckenridge and A. E. Petrie (Bell Telephone Laboratories, Inc., New York, N. Y.): Data presented in this paper indicate that brush temperature has a definite effect on brush wear, although when Doctor Ramadanoff visited our laboratories some six months ago, we understood him to say that his tests up to that time indicated that brush temperature had little if any effect. We presume, therefore, that the data shown in this paper result from tests made during the past six months and thus correct his earlier experiments. It is possible, however, that when Doctor Ramadanoff talked with us he was speaking of treated brushes, whereas the data shown in this paper appear from internal evidence to be for untreated ones. We should like to know therefore whether altitude treatment of brushes materially reduces the effect of temperature, and whether it makes it almost negligible up to 170 degrees centigrade.

In the section on "Commutator Film," the statement is made that, once the film of separation is removed, the rubbing surfaces of the brushes and commutator seize, and quick abrasions of both take place. If the abrasion takes place on the copper surface, it would seem as though the resulting roughness would mask the results of the test procedure used for showing the oxygen pressure required to stop rapid wear at various temperatures. In that series of tests, the cycle of reducing the oxygen pressure and promoting rapid brush wear and increasing oxygen pressure and stopping brush wear was repeated a number of times. Conceivably, the roughness of the commutator increased as the test proceeded and hence may have affected the results.

In field tests carried on ten years ago in which an a-c and a d-c motor both were coupled directly to the same load, we found that the brushes on the d-c motor wore much faster when the a-c motor was driving the load than when the d-c motor was driving it. In other words, wear appeared to be greater when the brushes were not carrying current than when they were. It would be interesting to hear whether Doctors Ramadanoff and Glass encountered this situation during any of their tests.

Jerome J. Taylor (Detroit Edison Company, Detroit, Mich.): The carbon brush has been an indispensable component of much electric equipment since the earliest days. Some of its behavior has been temperamental, which is equivalent to saying that the underlying principles are complicated and incompletely understood. This paper and other recent ones[1,2] add new chapters to an interesting history.

The present paper emphasizes mechanical phenomena. It is mentioned that the rings were stoned as a preliminary to most of the runs. Since the surface of a ring or commutator is smoother after wearing in than when freshly stoned, it seems reasonable to ask whether the aforementioned technique may have caused abnormal initial rates of brush wear.

A second question is whether ball or roller bearings have been considered for current collection from slip rings in high-altitude service. They could be made of materials having conducting and wear-resisting qualities, such as beryllium–copper, and it seems possible that they would operate satisfactorily with little or no lubrication.

REFERENCES

1 Carbon-Brush Contact Films, C. Van Brunt, R. H. Savage, *General Electric Review*, July and August 1944.

2 Sliding Contacts—Electrical Characteristics, R. M. Baker. AIEE Transactions, volume 55, 1936, January section, pages 94–100.

Harvey J. Finison (Wright Field, Dayton, Ohio): I would like to emphasize the importance of the high-altitude brush problem to the successful combat operation of military aircraft. All of the gun turrets in our military aircraft derive their power from a d-c motor in turn supplied by d-c generators. These units must operate continuously throughout high-altitude missions. Yet as recent as January of 1943, our aircraft were not equipped with machines with brushes capable of lasting throughout a single high-altitude mission of duration sufficient to bomb Berlin.

Measurement of generator brushes following each mission was a necessity on fighter aircraft operating in the South Pacific. After two or three missions, the brushes required replacement—a four- to six- hour job on some ships, since removal of the generator is necessary.

Fortunately, before the full seriousness of the brush-wear problem was realized, a solution to the rapid wear was found by men such as Doctors Ramadanoff and Glass, and others. The amount of effort expended by the brush and machine industries in arriving at a solution to the high-altitude brush problem deserves highest commendations.

The paper by Doctors Ramadanoff and Glass effectively presents some of the fundamental relationships in the rapid-wear phenomena and should be useful in giving a better general understanding of the problems.

While the figures presented on brush life attainable with ordinary and high-altitude brushes (Table III of the paper) are based on altitude-chamber tests, the high-altitude test flights conducted by the Army Air Forces at Wright and Patterson Fields have produced similar results.

I. E. Ross, Jr. (National Electrical Manufacturers' Association, New York, N. Y.): The papers presented by Doctor Ramadanoff and Mr. Summers are capable discussions of the problems and testing techniques involved in developing better electrical brushes for high altitude. They indicate a tremendous amount of effort which has gone in this project through the last two or three years. There is a danger, however, that the emphasis on difficulties tends to obscure the actual progress which has taken place.

I believe it is heartening to aircraft electrical engineers, who are responsible for the performance of electric equipment, to know that the elements for producing good high-altitude performance are available. At least six companies are capable of doing a satisfactory piece of work in applying new brush materials to motor application. The fact that motors currently available do not have improved performance at altitude is a result of a tendency on the part of designers to underestimate the difficulties which altitude can cause. Up to the present time the performance of generators, inverters, dynamotors, Amplidynes, and propeller pitch controls has been improved manyfold by a good application job on the part of motor and brush manufacturers working in conjunction with the armed services.

There is every reason to believe that any electrical device using brushes will respond just as favorably to a thorough engineering application of new techniques and new materials. There is no reason why aircraft electrical engineers should have to accept equipment having short altitude life.

On the other hand, motor and brush manufacturers are not justified in feeling that a problem has been completely solved. A brush life of 200 or 300 hours which is available today is good, but it is not in any sense of the word an objective. Development on the brush problem must continue until a product is developed which allows electric rotating machinery to bere placed on a maintenance schedule consistent with engine overhauling practices.

D. Ramadanoff: The discussion by N. T. Breckenridge and A. E. Petrie has brought out questions which are of interest in regard to high-altitude and sea-level application of brushes.

We have tested high-altitude brushes in dry air at pressures ranging from 1 to 760 millimeters of mercury and brush temperatures from 50 to 200 degrees centigrade. Long brush life was obtained under all of these conditions. Ordinary brushes under similar conditions will wear rapidly or dust. If the brushes and commutator, however, have not been preheated to drive out absorbed moisture, the brush and commutator temperature will have an effect on the pressure of dry oxygen at which rapid wear or dusting will begin.

We have never been able to establish experimentally that the finish on the commutator surface had anything to do with the rate of brush wear when unprotected brushes were tested in the vacuum chambers at reduced pressures of dry air. Our experience has been that the brushes, as well as the commutator, acquired a polish immediately after dusting of the brushes stopped.

The observation brought out in this discussion, that the brushes of a d-c motor wore much faster when the brushes were not carrying current, is indeed an interesting one. We presume that this observation was made under conditions of very low humidity. Current flow can decrease brush wear when sparking occurs, because of high-friction brush chatter. Under this condition we have observed the rate of brush wear reduced to one half of the wear rate obtained with no current flowing. However, if enough brushes were used in parallel to make the interruption of the current due to brush chatter negligible, current flow had no perceptible effect on the rate of brush wear. Recently we observed unstable operation on a gyromotor in high-altitude chambers. When ordinary brushes were used, brush wear was not excessive, but the instability was due to sparking which in turn was caused by chatter induced by high friction prior to rapid brush wear. When high-altitude brushes were used, the brush operation was steady.

The discussion by W. E. Campbell and G. T. Kohman brought out questions which are of theoretical and practical importance. In our tests with dry carbon dioxide, which was used to start dusting or rapid wear of the brushes, the pressure was atmospheric. Dusting was obtained with or without current flow. The brushes contained about 50 per cent copper, and brush pressure was about eight pounds per square inch. There were 24 brushes on this holder operating in four rows of six on four Everdur rings.

Rapid brush wear also was obtained with electrographitic brushes on a copper ring when the pressure of the dry carbon dioxide was 770 millimeters of mercury. This occurred with or without the flow of current. With current flowing a curious cyclic phenomenon occurred: The operation would alternate between a high-friction dusting condition and a low-friction nondusting condition. When the brushes were dusting, the high friction produced a high-pitch squealing noise. Under this condition the brushes were sparking, and the ring surface blackened in 15 to 30 seconds. Then the friction would drop suddenly to about one fifth of its former value, the squealing sound disappeared, the brushes no longer were sparking, and the rate of brush wear was small. In the next 15 to 30 seconds, the black film on the commutator would be removed, accompanied by rapid wear, high friction, sparking, and dusting. This cyclic phenomenon was self-repeating and self-perpetuating, taking approximately one minute for each cycle. In this experiment four brushes were used as described in the paper under "Vacuum Chambers."

We believe that Figure 7 of the paper, which shows the oxygen pressure at which dusting of brushes begins versus temperature, has been somewhat confusing, probably because of insufficient explanation. The purpose of this curve was to show that starting with the brushes and commutator at room temperature where moisture in the air condenses on both the brushes and the commutator, the oxygen pressure at which dusting would start would become greater as the brush and commutator temperatures increase with time.

Curve *A* in Figure 5 and its counterpart in Figure 6 show that rapid brush wear stops with an oxygen pressure of 19 centimeters of mercury. This value is comparable with 25 centimeters of mercury obtained at the Bell Telephone Laboratories, if we consider possible differences in brush pressure and brush materials.

The discussion by E. R. Summers has brought out several questions which are of general interest and need clarification. While specific values of coefficient of friction and wear at various pressures of oxygen atmosphere have been given for two carefully defined brush grades, in general, brushes which are not of the high-altitude type have one thing in common: namely, in an environment deficient in water vapor and oxygen they operate with high friction and rapid brush wear. This we believe is significant, and it is the intention of this paper to emphasize this point.

In all of the experiments used in preparing the data for this paper the brushes were heated to 300–350 degrees centigrade and the commutator to 135–150 degrees centigrade. Under these conditions, when the brushes begin to dust, the adsorbed-moisture film on the commutator is quickly removed, and there is very little adsorbed moisture left on the brush faces to affect the reproducibility of the measurements which have been obtained many times on different occasions. When the brushes begin to dust, the brush-face temperatures are high, much higher than indicated by thermocouples close to the brush faces, and are sufficiently high to drive off adsorbed moisture.

The importance of continuous flushing of an altitude chamber with dry air cannot be overemphasized. While it is true that self-ventilated machines will recirculate the cooling air several times before the air in the chamber is changed completely, we have never found that this had any adverse effect on rapid brush wear. In all of our tests, when machines were tested with ordinary brushes, rapid brush wear was observed in the matter of minutes, and the brushes were worn out in one to five hours. In fact, we have obtained equally rapid brush wear with brushes that were insufficiently protected for high-altitude operation.

In the operation of the altitude chambers it has been our observation that whenever the dew point of the dry air increased from −70 or below to −60 degrees centigrade, there was always visible frost on the walls of the chamber. Since the formation of this frost, which gives a grayish appearance of the chamber walls, takes place in a few hours the chamber walls have served as a rough dew-point indicator of the entering air. Dew-point measurements, of course, also are made periodically for more accurate checkups.

There is no excuse for building altitude chambers with noticeable leakage of room air. The altitude chambers described in this paper, pumped to a pressure of one millimeter of mercury and then sealed off from the pumps, will maintain a pressure of less than two millimeters of mercury for 24 hours. To obtain a tight chamber such as this, it was necessary to weld all joints with a triple weld.

We have had no experience on the question brought out by J. J. Taylor with regard to ball and roller bearings carrying current on slip rings at high altitude. We may point out, however, that metals unless properly lubricated also can wear rapidly under high-altitude conditions.

The Testing of Brushes for Life and Performance Under Various Altitude Conditions

Discussion of paper 44-230 by C. J. Herman, presented at the AIEE Los Angeles technical meeting, Los Angeles, Calif., August 29–September 1, 1944, and published in AIEE TRANSACTIONS, 1944, December section, pages 929–33.

Harvey J. Finison (Equipment Laboratory, Wright Field, Dayton, Ohio): One of the greatest problems in brush wear testing has been the selection of test conditions representative of service conditions. Selection of test conditions considerably more severe than actual operating conditions may result in elimination of brushes capable of giving excellent service performance, in fact, superior to those which may be selected on the basis of more severe tests.

Where test conditions are less severe than operating conditions, brushes may be selected which will give very short service life.

Of chief concern are operating loads and duty cycles. While these are more properly defined by machine rating, there are many cases where performance is specified at one value and life at some lower value of load more representative of use over a long period of service operation.

Establishing of ideal commutator condition and brush seat is probably essential for comparative testing. However, it should be recognized that at least in military service such ideal conditions cannot be expected where brushes are replaced in combat theaters.

As proposed by Mr. Herman, the testing of brush life should be a part of machine standards. A subcommittee of the air transportation committee is working on the preparation of standards for aircraft rotating machines. The methods proposed by Mr. Herman can and should be included in such standards.

D. Ramadanoff (National Carbon Company, Inc., Cleveland, Ohio): This paper gives a complete description of procedures which if followed with care will prevent many of the pitfalls likely to occur when simulating high-altitude conditions in brush and machine testing. The author has indeed had wide experience with a large group of machines of various types and ratings, as well as with brushes of different types and manufacture. This experience has been carefully included in the test procedure. In our laboratory during the early testing in the altitude chambers *NJ*-4 Amplidynes were used, the test procedure for which, in regard to loading, commutator tolerances and measurements of rate of brush wear had been previously worked out by C. J. Herman and J. F. Settle of the General Electric Company.

In regard to tests falling in class 2 and possibly class 3 of atmospheric conditions within the airplane we wish to introduce a word of caution. The atmosphere inside the airplane may be sufficiently contaminated to prevent rapid brush wear of ordinary and high-altitude brushes with insufficient degree of protection. Such has been the case in regard to some Amplidynes, inverters, dynamotors, and several other types of small machines. When these machines were tested in our altitude chambers with the brushes with which they came equipped, the brushes in most instances lasted 0.5 to 5 hours at an altitude of 34,000 feet and an air dew point below −70 degrees centigrade. Our tests at the time were referred to as "too severe" because of the lack of complaints of rapid brush wear on such machines in actual flights. But later the complaints did come, and high-altitude brushes had to be provided in a great hurry.

In a test at Patterson Field on an inverter which operated well inside the cabin of a *B*-24 bomber, the brushes wore out in one flight when outside air picked up by a scoop was brought to the inverter. All this brings to mind the effect on brush life of fresh air brought in the airplane through holes due to flak. We suggest that after the tests in an atmosphere such as in class 2 or class 3 are completed the degree of altitude protection of the brushes be evaluated by making at least a short-time test as in class 1. It is seldom that brushes with insufficient altitude protection have lasted in our high-altitude chambers longer than five hours.

The author has recommended that the brushes be seated "100 per cent of brush thickness for 50 per cent of the brush width." The importance of this statement can hardly be overemphasized. In uncompensated machines depending on the brush shift for best commutation, the brush thickness is a major factor in the satisfactory performance of the machine, particularly under high-altitude conditions where the contact drop is frequently lower.

In regard to the section on "General Precautions for High-Altitude Tests," we wish to add that the avoidance of any frost in the altitude chamber should be the ultimate goal. If altitude chambers are constructed in such a way that the chamber walls provide the refrigerating surface, contaminants will be more easily frozen out, and thus many inconsistent results avoided. With such construction, no cleaning of the chamber walls between tests would be necessary except for a thorough blowing away of the brush dust with compressed air.

New Test Chambers for Aircraft Electric Apparatus With Particular Reference to Carbon Brushes

Discussion and authors' closure of paper 44-231 by E. R. Summers and J. F. Settle, presented at the AIEE Los Angeles technical meeting, Los Angeles, Calif., August 29–September 1, 1944, and published in AIEE TRANSACTIONS, 1944, pages 1205–12.

W. C. Kalb (National Carbon Company, Inc., Cleveland, Ohio): Mention is made in the paper by E. R. Summers and J. F. Settle of the rapid brush wear experienced on synchronous-converter rings under conditions of very low humidity. This effect was known to most brush manufacturers. Therefore, when extremely short brush life first was encountered in high-altitude flight, it was but natural to assume that this was due, at least in part, to the low humidity known to prevail at high elevation. Although the view was held in some quarters that other factors were the major cause of high-altitude brush failure, the investigations made by Doctor Ramadanoff and Doctor Glass, as demonstrated at this meeting, provide convincing evidence that low humidity and meager oxygen supply are the major factors contributing to rapid brush wear.

In laboratory investigation of methods of protecting brushes against rapid wear at high altitude, the importance of eliminating frost-coated surfaces within the altitude chamber cannot be overemphasized. Doctor Ramadanoff has shown that the amount of water vapor needed to prevent brush dusting is very low. Contact of the warmer ambient air in the altitude chamber with frost-coated cooling surfaces readily can result in the absorption of sufficient water vapor to give favorable indication for methods of altitude protection which subsequent flight tests prove to be ineffective. It is gratifying to note that in the altitude chambers described by Summers and Settle provision has been made for the prevention of frost formation on all surfaces contacted by the air which reaches the commutator of the machine under test. Precautions taken to remove all other contaminating elements from the atmosphere are a highly commendable feature of these altitude chambers.

Fred A. Heddleson (nonmember; Westinghouse Electric and Manufacturing Company, Lima, Ohio): I believe that Mr. Summers and Mr. Settle have presented much interesting information on altitude chambers. Most interesting to me was the statement that five to ten complete changes of air per hour were sufficient to prevent accumulation of organic vapors. In our tests, we usually use a higher rate of change of air, more for the purpose of maintaining a lower dew point. I should be interested to know more about the tests conducted from which the value of five to ten was determined.

I should like to question the authors on their control of ambient temperatures. The ambient temperature at which the altitude chamber is held during tests on pressure-ventilated machines is dependent upon numerous factors. For instance, the position of cowl flaps affects the ambient temperature of the nacelle, and other factors such as the amount of air passing through the air coolers and oil coolers will cause the nacelle temperatures to vary.

Accurate duplication of altitude conditions requires that the ambient temperature at which a pressure-ventilated machine runs be reproduced as well as the temperature of the air entering the machine. On larger aircraft more and more use probably will be made of available space in the large wings for placing electric equipment that will require pressure cooling. In a case such as this, the ambient temperature required to reproduce altitude conditions would be very near to the free-air temperature.

I would like to ask the authors of this paper to what extent temperature regulation might be obtained in the two altitude chambers described, and what range of temperatures might be reproduced?

Mr. Summers and Mr. Settle have stated that: "If cold ambient temperatures are desired, the test machine may be surrounded by a temporary enclosure into which refrigerated air can be passed." From where is this refrigerated air obtained, and how is its temperature and quantity of flow regulated? The volume of air required for larger electric machines running at low ambient temperatures would reach proportions requiring an auxiliary refrigeration unit.

What provisions are available for such control of ambient temperatures?

V. P. Hessler (University of Kansas, Lawrence, Kans.): The authors and their associates are to be congratulated on the development and construction of such adequate and effective equipment for the study of sliding contacts. If such a laboratory had been available for testing aircraft equipment in 1941, it would have been worth as much as another aircraft carrier to the war effort, but none of us who were conducting brush tests at that time could foresee the enormity of the high-altitude brush problem. Precious weeks of development were lost while engineers waited for an opportunity to make inadequate tests in altitude chambers designed for other uses.

The inadequacy of simulated equipment for brush testing indicates how little we know of the fundamentals of sliding contacts and commutation. The intensive research on high-altitude brush wear has resulted in some important fundamental contributions concerning the mechanical properties of the contact, but very little fundamental information is available concerning the electrical properties of the contact. And finally knowledge of the manner in which a carbon brush aids in commutation is practically nonexistent. There is no present measure of the commutating ability of a brush other than trying it on a machine, and this is conclusive only after long periods of operation under varying conditions.

When the high-altitude brush problem is solved or developed to such an extent that it no longer carries top priorities, these laboratories and the enterprise which developed them undoubtedly will be applied to these other fundamental aspects of the brush problem. The goal is the development of a few grades and holders which can be applied to a machine with the same certainty of performance that we now expect in lubricating the bearings.

S. I. Pearson and **E. F. Hansen** (General Electric Company, West Lynn, Mass.): Prior to the completion of the two large chambers described in the paper for testing pressure-ventilated machines, high-altitude brush testing of such machines, totaling several thousand hours of operation, were made in a small test chamber having a free working space 24 by 24 by 30 inches. The air is recirculated, a small vacuum pump with automatic control being used to take care of a small amount of air leakage. An adjustable-speed Roots-type blower inside the chamber supplies the air blast for the generators. This blower is externally driven, having its rotating-shaft seal built into the blower unit. This chamber accomodates six-kilowatt generators driven by a shaft passing through the chamber wall at speeds up to 5,000 rpm. A vacuum seal at the rotating shaft consists of a modified bellows-type shaft seal with a steel rotating surface riding on impregnated graphite and lubricated by low-temperature oil. The small amount of oil leakage is prevented from entering the chamber by pumping the leakage air from the chamber through this trap.

Air in the chamber is refrigerated by conventional fin-type evaporator coils and by evaporator plates on four walls. Additional evaporator coils refrigerate the generator air blast preceding and following the blower. The air blast into the generator at a chamber pressure corresponding to 35,000 feet altitude can be maintained at a temperature of −50 degrees centigrade at light generator loads and at −35 degrees centigrade with full load on the generator. Freon 22 is the refrigerant used in the three-stage 22.5-horsepower refrigeration equipment.

The chamber has a large multilayered glass door which is very advantageous for observing generators and slip rings under test. Brush sparking is readily observed. The rate of brush wear can be observed by dial indicators mounted so as to indicate the shortening of the brush in thousandths of an inch. Electrical leads and thermocouples through the wall of the chamber are provided.

The humidity of the chamber is held at a low value by recirculation of air through the evaporator coils which operate at a temperature of from −55 to −65 degrees centigrade. Contamination is held to a minimum by washing the chamber walls and blowing out ducts after each test. The chamber is equipped with heaters which are used to dry out the chamber thoroughly before each test. All thermal insulation is outside of the chamber.

The door cannot be opened in the middle of a test without first warming up to room temperature, because moisture in the room air would "ice" the chamber. Warming the chamber requires one hour. Refrigerating the chamber to −40 degrees centigrade with no generator load requires $1^{1}/_{2}$ hours.

By means of careful test procedures using this chamber, brush tests made on generators so far have correlated well with results obtained on actual flight tests. However it is realized that actual flight conditions are not fully simulated in that fresh clean air is not being supplied continually to the generator, and there is not adequate control of ambient-air temperature. We believe that the chambers described for testing pressure-ventilated machines represent a real improvement over the recirculating-air type, not only from the flight-similitude standpoint but from the standpoint of ease of operation and maintenance.

We agree heartily with the writers, on the basis of the results of our tests, that the ultimate test of any brush is its operation in the electric apparatus in which it is to be used. Sea-level tests of high-altitude brushes are necessary, since the performance (either good or bad) of a brush at high altitude is no indication of its performance at sea level. Brush tests of a few hours duration very often are not conclusive, because changes of commutator surface often are gradual, and many hours may elapse before there is a noticeable change in brush operation. Short tests are apt to furnish optimistic conclusions.

Brush tests of electric apparatus should be made from minimum up to the maximum rated speed and from no load up to full rated load of the apparatus. The cooling-air and ambient-air temperature and pressure should be the same as in actual flight, so that the brush and commutator temperatures in the test chamber correspond to brush and commutator temperature in actual flight.

E. R. Summers and **J. F. Settle:** F. A. Heddleson has asked about the number of air changes per hour that is needed to prevent accumulation of organic vapors in an altitude chamber that is used for carbon-brush tests. Actually, this depends on the volume of the chamber compared to the ventilation requirement of the machine being tested, on how well the paint and insulating varnish are cured, on the type of grease in bearings, on whether the test unit is totally enclosed or self-ventilated, and so on.

The five or ten changes per hour mentioned in our paper referred to the chamber of Figure 6 of the paper which has been used for tests on small totally-enclosed or self-ventilated machines only. This chamber has a volume of approximately 35 cubic feet. A typical self-ventilated one-kilowatt motor-generator set takes in approximately 1,200 cubic feet of air per hour. Ten changes of chamber air per hour would correspond to 350 cubic feet of fresh air admitted. The same air would pass through the test unit an average of 1,200/350 or about 3.5 times before being evacuated from the chamber. Several hundred tests thus made have correlated well with flight tests. When self-ventilated units are installed in aircraft, there is some recirculation of the same air also. The inside surfaces of the aforementioned chamber are carefully cleaned after each test to minimize accumulation of foreign material.

When pressure-ventilated generators in the large chamber of Figure 9 of the paper are being tested, the ambient temperature can be controlled through a wide range. The effects of the cold-air duct to the generator and of the hot air discharged from the latter commonly give an ambient temperature of 20 to 30 degrees centigrade around the outside of generator frame. If a higher ambient temperature is desired, a heater unit can be placed in the chamber or a deflector provided to pass part of the hot discharge air over the outside of the generator frame. To obtain ambient temperatures from about +30 to 0 degrees centigrade, part of the refrigerated air is passed through the generator, and part is throttled directly into the main chamber. The ambient temperature is controlled by the amount of air throttled and by the point of entry, that is, near the generator frame or near the exhaust line to steam ejectors.

To obtain lower ambient temperature, a small partial enclosure can be placed around the test unit and cold air throttled into this enclosure. Temperature is controlled by rate of air flow and by insulation, if any, on the outside of the temporary enclosure. With such an arrangement, −40 degrees centigrade ambient temperature was obtained readily for voltage-regulator tests, without refrigerating several tons of steel in the walls of the main chamber.

In addition to cooling the blast of air admitted into the aircraft generator undergoing the test, the refrigeration system of Figure 8 of the paper has enough surplus capacity to control the ambient temperature also, or to test simultaneously other auxiliary equipment. By tapping the cold-air supply line ahead of the generator, a pressure of several inches of water is made available for distributing this refrigerated air to other desired locations within the chamber.

The Development of Aircraft Position Lights

Discussion of paper 44-206 by Jack Vitol presented at the AIEE Los Angeles technical meeting, Los Angeles, Calif., August 29–September 1, 1944, and published in AIEE TRANSACTIONS, 1944, November section, pages 796–8.

P. H. Greenlee (nonmember; lieutenant, United States Army Air Corps, Wright Field, Dayton, Ohio): Present position-light practice on military aircraft differs slightly from present Civil Aeronautical Authority requirements. The majority of existing military aircraft are equipped with the position-light system in effect by the Civil Aeronautical Authority before the advent of the flashing lights: that is, one red and one green wing-tip light and one white tail light. Basic and advanced training airplanes are equipped further with a red passing light on the leading edge of the left wing. These are still being installed in new training airplanes. Practically all of the military aircraft of recent production employ the standard air-line type of wing-tip light; however, many of them are using smaller lower-candlepower tail lights.

Army Air Forces airplanes permanently assigned to activities within the continental United States, except fighter types, may be modified to include a flasher mechanism in the position-light circuit at the request of the using organization. No more lights are installed in this modification, and the two wing-tip lights flash simultaneously and alternate with the tail light at a rate of 40 cycles per minute. There is a switching arrangement provided to permit the pilot to operate the lights momentarily or steadily in addition to the flashing cycle.

A special requirement exists for wing-tip position lights on fighter types of airplanes. These types of airplanes are not equipped with formation lights with a result that wing-tip light visibility through an angle of 180 degrees in lieu of 110 degrees is considered necessary, so that the wing-tip lights can be used as formation lights.

At the present time the Army Air Forces is studying its position-light problems in an effort to determine what further changes or improvements should be made.

Airplane Engine and Propeller Test-Cell Lighting

Discussion and author's closure of paper 44-233 by Davis H. Tuck presented at the AIEE Los Angeles technical meeting, Los Angeles, Calif., August 29–September 1, 1944, and published in AIEE TRANSACTIONS, 1944, pages 1264–70.

F. C. Somers (nonmember; Scotch Plains, N. J.): Mr. Tuck's paper, in stressing the importance of co-ordinating the brightness of the control-room and test-cell lighting, underlines the fundamental cause of many of the existing unsatisfactory lighting installations. Many test cells, having good lighting in the control room and test cell considered separately, have failed under conditions requiring the engine to be viewed through multipaned glass observation windows.

Mr. Tuck, in his sample calculations, assumes an initial control-room brightness of ten foot-lamberts or 21 foot-candles, which figure appears entirely reasonable. Based on a 25 per cent reflection factor of the engine, a minimum of 90 foot-candles is required on the engine to match the ten-foot-lambert brightness of the control room. With these intensities as a goal, Mr. Tuck calculates the number, location, and wattage of the fixtures required to produce the desired brightness. Although the calculations are excellent, the solution offered has many practical disadvantages.

The initial cost, as well as the operating cost, of the large number of lighting fixtures required for a 90-foot-candle intensity in the test cell is quite high. Dynamometer-type cells usually have a number of obstructions such as cooling-air ducts, carburetor-air ducts, overhead cranes, and others, which make it difficult to concentrate a large number of fixtures in the vicinity of the engine. Furthermore, a large number of flush-type fixtures interferes with and is apt to reduce the effectiveness of the accoustical material generally used to line the walls of the cell. When mechanics are working on the engine, the radiant heat from a concentration of lights may become objectionable, especially in the summer months.

It has been the experience of the discusser that an initial intensity of 40 foot-candles at the engine is quite satisfactory for setup and mechanical work. When such a test cell was designed, extension lights were located on either side of the engine to aid in close work. It is interesting to note that the illumination provided by the fixed lights has been so satisfactory that the mechaincs never use the extension lights. The use of tail pipes and careful location of the engine to keep the exhaust stream as far from the observation window as possible, minimize the loss of brightness from dirty glass, when the cell is viewed from the control room.

Control rooms are becoming increasingly difficult to illuminate. Instrument layouts are becoming more elaborate and panel boards parallel to the observation window are becoming too long to permit a view of all instruments from a central point. In order to keep all instruments close to the operator the trend is to add wings at either end of the panel. These wings are at an angle to the observation window. No difficulty is experienced in obtaining glareless lighting of the panel wings, but it is very difficult to prevent "veiling glare" on the observation window because the wing lights often must be located three or four feet out from the window.

The problem is complicated further by the fact that any increase in control-room brightness must be matched by a corresponding increase in test-cell brightness, which has a number of objections as just outlined. This difficulty may be overcome by designing engine illumination for about 40 foot-candles and co-ordinating control-room illumination by reducing its brightness. By this system, when the test cell is in operation, all overhead lights in the control room are turned off and special lighting is employed to illuminate the instruments and controls. All glass tube devices, such as manometers, are lighted from the rear of the panel. The scales for these instruments are of special design. The front of the scale is made of black opaque plastic, and numbers and graduations are cut out. The resulting black stencil then is backed with a sheet of white translucent material. With this type of scale the lighting behind the manometer tubes also causes the numbers and graduations of its scale to be illuminated. Small instruments such as pressure gauges, ammeters, voltmeters, and so forth are equipped with black scales with white marking and illuminated either with individual lights within the instrument case or by exposing a fluorescent scale to an ultraviolet source. Recording instruments and log sheets also are lighted individually from within the case, but because of the high reflection from large white paper areas the devices are shielded as much as possible to minimize glare.

This type of control-room lighting eliminates "veiling glare" on the observation window, makes all details of the test engine stand out sharply (because it is very bright compared to the operator's surroundings), assures good illumination of each instrument, does not require an excessive brightness in the test cell, and is economical to install and operate.

John R. Immel (nonmember; Wright Aeronautical Corporation, Paterson, N. J.): It seems to the writer that the problem presented in this paper is one of the most important in aircraft engine testing. To extract efficient labor from employees, it is essential to have harmony in the group, and to have this, it is necessary to make working conditions as favorable as possible. First among these comes: the ability to see what you are working with.

Under the heading of "Illumination Requirements" in the paper there was quoted a life of 200 hours for test-cell light bulbs. We have had considerably longer life from our bulbs. For example, in one group of four 20-foot-square test cells we have group averages of from 333 to 666 actual engine-running hours, or 1,310 to 2,620 total hours life. These bulbs are installed in flush-mounted Holophane fixtures and Pyle-National spotlights.

In the paper the first paragraph under "Control-Room Calculations" states that the ideal location for the ceiling lights should be six inches in front of the front edge of the control board. This should be qualified to bring into the picture the possibility of a sloping panel on the control board.

The method of shock mounting, as mentioned in the paper under "Mechanical Requirements," should be investigated more thoroughly from the standpoint of a mount and assembly with a natural frequency well below any operating frequencies to be encountered. For the amplitude of vibration is smaller when the engine is operating above the natural frequency than when it is operating an equal amount below.

Another item which might bear looking into more thoroughly is the heavy-filament low-voltage "railroad" bulbs.

In the writer's opinion, the ideal test-cell lighting installation would be:

1. Provide individually illuminated instruments with bulbs either behind the instrument face or mounted inside the case in front of the instrument face.

2. Two circuits for control-room lighting, one for general work in the control room, when no engine is in operation, (adequate light), and the other for illumination when the engine is in operation (dim light).

3. Two circuits controlling test-cell lights, one for setup work (adequate light over entire area of the cell) and the other for engine operation only (spot or flood the engine setup only).

Davis H. Tuck: Mr. Somers' deduction that the necessary foot-candles of the engine may be reduced by reducing the foot-candles of the control-room instruments and so forth is correct. However, a reduction would not be an equal one for both engine and control room because of the different factors which reduce illumination to brightness for the two rooms. He is also correct in saying that approximately 40 foot-candles on the engine for setup work is satisfactory. My calculation shows 50 per cent depreciation *after* four hours of engine operation, which would bring the necessary illumination without this depreciation to 45 foot-candles. I doubt that the operators would complain about the heat of 90 foot-candles. Doctor Luckiesh conducted blindfold tests which indicated the minimum illumination that barely could be detected by a sensation of warmth was 125 foot-candles from 100-watt incandescent lamps. Contrary to Mr. Somers' remarks, I have found no particular difficulty in locating the required number of lights in a test cell from either the mechanical or cost standpoint (see Figure 12).

Mr. Immels' discussion is very interesting and shows a keen appreciation of the problem. His low figure of 333 checks fairly well with my 200-hour life of lamp. Of course we would expect longer life with smaller engines and with good installation methods. The reasons for the six-inch dimension for control-room lights are to secure vertical illumination, to avoid *all* direct glare, and to keep the light between the limits of angles that will cause sheen on the window.

There always will be some engine speed that will correspond to the free period of the lighting unit. It is best not to have the free period correspond to the normal engine speed. There are two kinds of vibration that must be damped, one that is transmitted through metal parts and the other that is transmitted through the air.

The low-voltage railroad lamps are not available in the wattage sizes required for test-cell service. The low-voltage street series lamps might become useful by operating them in parallel on constant voltage of the proper value, instead of in series on constant current. I will investigate this possibility.

Both Mr. Somers and Mr. Immel suggest the use of integral instrument lights. If this be done, care must be taken to limit the brightness so that it will not be greater than that of the engine.

Basic Considerations in the Selection of Generators and Batteries for Aircraft

Discussion and authors' closure of paper 44-204 by L. M. Cobb and H. M. Winters, presented at the AIEE Los Angeles technical meeting, August 29–September 1, 1944, and published in AIEE TRANSACTIONS, 1944, December section, pages 889–93.

C. E. Wood (nonmember; captain, United States Army, Wright Field, Dayton, Ohio): The method in the paper is an excellent approach to the problem and is sound in its reasoning. However, the ultimate results obtained in any such calculation cannot be more accurate than the assumptions upon which it is based, and in the case of military aircraft there cannot be an accurate basis for these assumptions.

This is a result of the unpredictable manner in which the aircraft are used. Conditions are different in each theater of operation, and it is not known for certain at the time designs are in process to which theaters the aircraft will be assigned. But, nevertheless, predictions of operation of the electric apparatus must be made to solve the problem as set forth in the paper.

Assumptions and predictions are opinions, and in meeting the problem the various designers obviously will use their best judgment, but, more likely than not, no two will base their calculations upon the same assumptions.

It would be highly desirable to have an operating timetable indicating customary use of the various pieces of electric equipment in the aircraft under its various conditions of flight, but this is not possible as all designers must resort to their judgments based on all the experience that can be gathered.

This brings out the need for agreement between the various designers of electric systems for aircraft as to the time of operation that will be assigned to each piece of equipment for the manner of flight or each type of mission to be considered. Such agreement should be the result of gathering the experience of those who are familiar with present operations of similar types of aircraft coupled with the intended end use of the aircraft design and its electric equipment.

The other factor which affects the accuracy of the load analysis is the estimated values of current demand assigned to each piece of equipment. It is realized that such current values are not always available from manufacturers of equipment at the time the design of the electric system is under consideration and that some of the current values may have to be estimated or the design figures of the manufacturers used, if the equipment has not been tested. Furthermore, equipment furnished to specifications may be supplied by two or more manufacturers, and the current consumption of the products of the different manufacturers may differ. This problem exists on present aircraft and applies to the engine-driven generators as well.

Therefore, when the predicted load on any aircraft is matched with generating capacity, it is essential that designers should not adhere too closely to the line and that a reasonable margin be allowed. On military aircraft this is especially important because of the numerous modifications which these aircraft undergo, which in nearly all instances add electric load. There are other factors of design which affect the selection of generating capacity, such as provision for operation of the aircraft with one or more failed generators in the case of multiengine aircraft and the provision for fault protection.

The subject paper points out the necessity for considering the rms value of peak loads, and it cannot be said that they should not be investigated when the design is being considered; however, they take on less importance as generating capacity is increased to allow for the margin in generating capacity just mentioned.

The authors, in their paper, have chosen two periods, two minutes and ten minutes, for calculating the peak, average, and rms values of current. Their method lends itself to calculating these values for any and all periods of operation of the equipment, and Table I of this discussion has been prepared from their Table I to show the differences in these values for each accumulated period. For example, the summation of the loads at the end of 1.5 minutes gives an

Table I. Average and Rms Values for Ten-Minute Period

Elapsed Time, Minutes	Total Peak Load, Amperes	Time Duration of Peak Load, Minutes	Increment for Period, ΔI Amperes	Ampere-Minutes I_t	Accumulated Ampere-Minutes	Average Load for Period, Amperes	Rms Value for Period	
							I_t^2	Rms
0.048	456.6	0.048	253.1	21.92			10,008.6	
0.16	203.5	0.112	2.0	22.79	44.71	279	4,637.8	302
0.33	201.5	0.17	2.3	34.26	78.96	239	6,903.4	256
0.75	199.2	0.42	33.7	83.66	162.62	217	16,665.1	226
1.00	165.5	0.25	2.0	41.38	203.99	204	6,848.4	212
1.50	163.5	0.50	106.8	81.75	285.74	191	13,366.1	197
2.50	56.7	1.00	2.6	56.70	342.44	137	3,214.9	157
5.00	54.1	2.50	7.0	135.25	477.69	96	7,317.0	117
10.00	47.1	5.00		235.50	713.19	71	11,092.0	89

Table II. Average and Rms Values for Two Periods

Period	Accumulated Ampere-Minutes	Average Load for Period, Amperes	Rms Value for Period	
			I_t^2	Rms
0–1.5		191.0		197.0
1.5–10	427.45	50.2	21,623.94	50.4

average current of 191 amperes, and the rms value is 197 amperes. For the ten-minute period, the values are 71 and 89 respectively.

If the period were divided in 1.5- and 8.5-minute periods as shown in Table II, the average values would be 191 and 50.2 amperes and the rms values 197 and 50.4 amperes respectively. The rms value for the second period does not reflect the residual heat in the generators, if it be assumed that the peak for the first period has just been removed. The method shown in Table I does reflect this condition. Calculating these values for all time periods of all conditions of flight is extremely laborious by the usual methods and consumes considerable engineering time, although it is desirable. In lieu of this, it would be desirable for all manufacturers of aircraft generators to rate them on definite periods such as suggested by the authors.

Harvey J. Finison (Wright Field, Dayton, Ohio): Electric-power-system capacity is established by three considerations:

1. Thermal limitations of supply machines.
2. Maximum steady-state limit of supply machine.
3. Maximum allowable voltage dip during motor starting.

Mr. Cobb and Mr. Winters have discussed effectively methods of establishing the thermal capacity requirements by obtaining the rms value of electric loads varying over a period of time. For aircraft machines the periods under consideration will be very short compared with industrial practices because of the low-thermal-storage capacity of the machines. Maximum periods of time will not exceed 20 minutes, while the minimum may be the five-second period mentioned in the paper.

The use of the term "unstable" to describe the limit in steady-state capacity may cause some confusion in visualizing the nature of that limit. As the demands of the load increase beyond the limit established by the system current–voltage characteristic, stable operation at subnormal voltages still is obtained, if the character of the load is such that the load decreases with voltage at a faster rate than does the supply characteristic. Collapse of excitation results only when the current–voltage demands of the load are not contained within the boundaries defined by the system current–voltage characteristic.

Batteries are a very uneconomical method of increasing the steady-state capacity of the system even for loads of short duration.

Depending on operating speed and the temperature previously attained, a 200-ampere generator usually can be considered as capable of delivering 300 amperes for at least 30 seconds at full voltage. Batteries can deliver current only in response to a reduction in voltage. If we consider that the lowest permissible voltage limit for a short period is 20 volts (or a 28-volt system), a 34-ampere-hour aircraft generator can deliver only 170 amperes at 80 degrees Fahrenheit and 100 amperes at 20 degrees Fahrenheit for five seconds. At lower temperatures and for longer periods the output is considerably less. Further, if the load is of the character of a shunt motor, the current demand at the lower voltage may be higher—a 120-ampere load at 28 volts increases to 170 amperes at 20 volts for a constant-power load.

From these considerations it can be seen that in order to equal the steady-state current output of a single 200-ampere generator, two 34-ampere-hour batteries would be required even at the most advantageous temperatures.

The weight of a 200-ampere high-speed engine-driven generator with its controls and control wiring is approximately 50 pounds, whereas two 34-ampere-hour aircraft batteries with controls weigh 160 pounds.

Where weight has to be charged to the generator prime mover, as for an auxiliary power plant, the comparison is not so strikingly in favor of generator capacity.

Considerable work is being carried out in establishing transient characteristics of supply and load machines and in developing methods of calculating voltage dips during transient loads such as occur during motor starting. The minimum initial voltage following application of a heavy load is determined by the supply-machine characteristics, character of existing electric loads, and character of the load applied.

Consideration must be given to the minimum permissible voltage during the selection of the supply units.

L. M. Cobb and H. M. Winters: Mr. Finison discusses the addition of batteries to increase steady-state capacity of a system. The addition of batteries specifically for this purpose, even for short periods of time, is unquestionably uneconomical from a weight standpoint. However, large magnitude transient currents of a nature normally occurring when equipment, such as motors, is energized usually endure for only a small fraction of a second and may be handled conveniently in part by the battery furnished for engine starting. This can be accomplished with no additional battery capacity, providing the current peak does not exceed the condition of instability as dictated by the system volt-ampere characteristic. Under overload conditions of this nature the generators and battery supply current to the load as functions of the system voltage in accordance with their respective volt-ampere characteristics.

Captain Wood's discussion brings forth the need for standardization of operating times and equipment-unit current requirements in order to obtain reliable basic information for analyzing aircraft electric loading. It also discusses the diversity of current data for a given type of equipment produced by several manufacturers to the same specifications. These facts have been known and the need for more reliable data has been realized, but in its absence it has been necessary to use available information judiciously with certain assumptions.

In the past it has been found that the assumptions have been made with a fair degree of accuracy and that the required marginal allowance to compensate for errors in the assumptions has been relatively small. This is particularly true when load-current values have been determined by rms calculations, since this method presupposes pessimistic assumptions about conditions of loading.

A 120-Volt D-C Aircraft Electric System

Discussion and author's closure of paper 44-203 by L. M. Cobb, presented at the AIEE Los Angeles technical meeting, Los Angeles, Calif., August 29–September 1, 1944, and published in AIEE TRANSACTIONS, 1944, pages 1327–34.

S. R. Bergman (deceased; formerly with General Electric Company, Lynn, Mass.): This article is of particular interest to the writer who designed and built the generators for the Mars flying boat. It is a valuable contribution as it describes the actual performance of a 120-volt d-c system for airplanes.

This equipment was designed in 1940, and since then we have learned a great deal, both about the generators and the control. Having recently obtained information from the log of this ship, I agree with the author that very little trouble can be ascribed to the 120-volt d-c system. Some trouble with the auxiliary generators was encountered, and it was found to be caused by faulty machining of the auxiliary engines, resulting in a bad line-up of the bearings. Since this was corrected, no further difficulties have been encountered.

In comparing the 120-volt d-c system with the a-c polyphase system, I want to point out one feature. Because of the speed variation of the engine, which is more than 2:1, the frequency of the alternators also changes 2:1. With a constant-potential system, this would be unsatisfactory for the operation of induction motors; hence, it has been found necessary to provide a unit which transforms the varying speed of the engine into a constant speed of the generators. Such devices are based on the hydraulic design and represent an extremely difficult problem. This constant-speed device is quite heavy and is comparable in weight to the alternator with its exciter.

A great deal of discussion has arisen as to the relative merits of the 120-volt d-c system as compared with a 208-volt 400-cycle three-phase system. The relative weight of the two systems depends upon the nature of the utilization apparatus on the airplanes. Airplanes having a preponderance of loads requiring alternating current, such as radio transmitters, receivers, radar, and so forth, are favorable to the application of alternating current, since weight saving can be shown by elimination of conversion equipment. On the other hand, if the load is predominantly of the nature of motors and the like, the d-c system probably will be the lightest.

Problems of brush wear, commutation in rotating machines, and also arc interruption in switchgear at high altitude will be encountered in designing equipment for 120-volt d-c systems. Some work has been done along these lines, and the problems do not look as formidable as they might appear at first.

I am inclined to believe that a satisfactory 120-volt d-c system can be developed both for medium and high altitudes and that such a system would be lighter than the three-phase a-c system in many cases.

R. R. Miille (Westinghouse Electric and Manufacturing Company, Lima, Ohio): Information on 120-volt d-c systems for aircraft has been overshadowed somewhat by publicity given proposed a-c systems. Mr. Cobb's paper, however, gives the results of a 120-volt d-c system in actual service, and it is especially important, as it is one of the first real trials of this system on an airplane. The service results show that the 120-volt d-c system is satisfactory. As this system becomes more common, the development of 120-volt d-c electric aircraft equipment no doubt will bring out improvements that will give even better operating conditions and possible weight savings.

In many papers and discussions on aircraft electric systems, the use of 120-volt direct current is claimed to be unsatisfactory at high altitudes. This has been most prevalent among proponents of a-c systems, but Mr. Cobb has given a similar inference in his conclusions when he states that 120-volt d-c systems are practical, providing the airplane operates at low or medium altitudes. No doubt, he merely wanted to limit his conclusions to the altitudes covered by the operation of the Mars. There is, however, no evidence that the operation need be limited to these altitudes. As test results are being obtained, they are beginning to show that these claims often are based on erroneous opinions due to lack of proved information. Tests now have been made which indicate that 120-volt d-c systems will operate satisfactorily at higher altitudes. The AIEE paper by W. L. Berry and J. P. Dallas[1] gives results of such tests and shows that 120-volt d-c systems are practical at all altitudes. It also shows that they will effect weight savings beyond that of other systems.

Tests have been taken in our altitude chamber at Lima on a 120-volt d-c generator. The tests were conducted at altitudes up to 60,000 feet in steps of 10,000 feet. The generator was operated at 120, 160, and 200 volts at rated load. The testing time was one-half hour for each condition. The operation was satisfactory under all conditions except at 200 volts and 60,000 feet where the brush temperature became excessive and slight sparking showed at the brushes. The brushes were the same as used on one of our 24-volt aircraft generators and have an altitude treatment developed by Doctor Elsey and his associates at the Westinghouse Research Laboratories. It should be explained that altitude tests of generators in this chamber have been correlated with actual flight tests and found to compare very closely, the only difference being that the chamber tests are a little more severe than actual flights. Consequently, any equipment passing the chamber test should operate even better in flight.

These tests indicate that the commutation of 120-volt machines at high altitudes, which so often has been referred to as a limitation to the use of 120-volt d-c systems, is no more of a problem than at 24 volts, which now is being used at high altitudes with entire satisfaction.

The difficulties experienced in the development of a-c systems are focusing attention to 120-volt d-c systems. This is encouraging, as it will tend to equalize the development work done on each system, thus allowing a fair comparison which will assure the use of the more adaptable system to airplanes of the future.

REFERENCE

1. HIGHER-VOLTAGE D-C AIRCRAFT ELECTRIC SYSTEMS, W. L. Berry, J. P. Dallas. AIEE TRANSACTIONS, volume 63, 1944, November section, pages 843–9.

L. M. Cobb: The discussion of this paper by Mr. Miille reveals the results of some laboratory tests made on high-voltage d-c equipment under high-altitude conditions. The favorable results of these tests supplement the observed performance records of the Mars installation and contribute to the disproval of some of the disadvantages commonly associated with high-voltage d-c systems.

To clarify the questionable meaning of the writer's conclusion noted in Mr. Miille's discussion, conclusions were based on actual operating experience, and it was meant to indicate that satisfactory operation could normally be expected for altitudes up to 16,000 feet. However, it was not intended to imply that operation at higher altitudes would be unsatisfactory.

In both prepared discussions it is indicated that efforts are being directed with satisfying results toward the solution of problems incidental to high-voltage d-c systems. This is encouraging, since less attention has been given to the development of high-voltage d-c equipment than to high-voltage a-c equipment because of theoretical disadvantages in connection with the use of the former at high altitudes.

Since certain types of loads can be supplied more economically by alternating current and other types of loads are better adapted to direct current, it would be of advantage to aircraft designers to have high-voltage equipment developed to a state of refinement such that it would be possible to select the type of system most suitable for the loads of a particular airplane design.

Impedance of 400-Cycle Three-Phase Power Circuits on Large Aircraft and Its Application to Fault-Current Calculations

Discussion and author's closure of paper 44-237 by C. K. Chappuis and L. M. Olmsted, presented at the AIEE Los Angeles technical meeting, Los Angeles, Calif., August 29–September 1, 1944, and published in AIEE TRANSACTIONS, 1944, pages 1213–20.

L. G. Levoy, Jr. (General Electric Company, Schenectady, N. Y.): The authors have presented an interesting paper which should be of considerable use in calculating the performance of three-phase 400-cycle a-c power systems in aircraft.

The paper is of particular value in establishing values of zero-phase sequence reactance which will serve as a check on engineering approximations which usually are made for this quantity.

Fortunately, it usually is not necessary to make precise calculations of line reactance to determine fault currents in such systems, since they can be evaluated closely enough for most purposes by simple calculations based on the following considerations:

1. For all small wire sizes (*AN* 8 and smaller) with minimum spacing, the resistance is the predominant factor.
2. For larger wire sizes, the runs are usually sufficiently short so that their impedance, is small compared with the machine impedance and small errors in the calculated reactance result in negligible error in the calculated fault currents.

In connection with Table VI of the paper, it appears that r_g has been calculated by subtracting the tabulated values of a-c resistance of three wires in parallel (based on the maximum permissible value of a-c resistance of a given wire size) from the a-c resistance of three wires and ground return determined by test. Actual wires normally have a resistance which is slightly under the maximum permissible value, to allow for some tolerance in manufacture. Although this difference usually can be neglected, it does not seem proper to do so in attempting to evaluate r_g by this method, since r_g represents the difference of two relatively large quantities. It would be preferable to use a value of resistance based on the measured resistance of the actual wire sample used for this purpose. It may be that some of the random variations in the r_g column of Table VI are due to this influence.

A negative value of r_g, as shown at one point in this table, is impossible, since r_g is separable from the resistance of the outgoing conductors. The quantity r_g, which was to be evaluated, is evidently so small that it has been masked by other factors and the average of this column cannot be considered as a reliable guide to its actual value. A negative value of x_g is permissible, since it is not possible to separate x_g from the reactance of the outgoing conductors. However, it should change uniformly, though slowly, with wire size.

The value of d-c resistance given for the fuselage is higher than a-c resistance value given. This result is inadmissible since direct current spreads out in such a manner as to follow the current distribution which will give minimum resistance. Since alternating current tends to follow a different distribution (that for minimum impedance), d-c resistance never can be higher than a-c resistance.

The actual value of d-c resistance given appears to be abnormally high for a well-bonded aluminum skin and structure, values of one tenth this value or less being frequently observed in aircraft structures. It would be of interest to know whether the connections for resistance measurements were made to primary stress-carrying members or to the skin, how the measurements were made, whether or not they include contact resistance, and the nature of these contacts.

The comments regarding the small factors mentioned here are offered to help clear up any questions which apparent discrepancies in the measurements may raise in the reader's mind.

C. K. Chappuis: The design of 400-cycle aircraft circuits for certain aircraft was completed at Wright Field by the writer and Mr. Olmsted. This included voltage regulation, wire carrying capacity, fault currents, fuse co-ordination, and circuit-breaker requirements and design.

Because of the rigid co-ordination requirements, it was determined that we could not tolerate appreciable errors in fault-current calculations. Since the alternator resistance is 0.0232 ohm and the positive, negative, and zero-sequence impedances are respectively 1.46 ohms, 0.147 ohm, and 0.094 ohm, it is obvious that additional resistance in the circuit would change the impedance very slightly, whereas an increase in reactance would increase the impedance as an approximately linear function under conditions in which the fault is located comparatively close to the alternators. This is particularly true as these portions of the circuit are of the greatest conductivity.

The point where the fault currents are controlled by the terminal voltage or the internal voltage of the machine is very critical and is affected greatly by the circuit constants. Therefore we believe that greater, instead of less, accuracy in impedance values is desirable and that additional test work is necessary to determine these values with greater accuracy.

It is believed that the charges as to errors in the values of r_g are recognized and can be explained. A test program was arranged to determine by use of laboratory instruments the values of zero-sequence impedance in an existing large airplane. This was necessarily limited in time and scope by the urgency of design requirements as determined by the aircraft design; also by the scarcity of suitable test equipment and aircraft structures. The objective was to obtain sufficient data to verify the results of, and justify the use of analytical methods for obtaining zero-sequence impedance values satisfactory for immediate use in the design projects. Inasmuch as the zero-sequence component is only one of three values used to determine ground fault currents, it is apparent that small errors can be tolerated, as the value of the fault current is not a direct function of the zero sequence impedance. It is believed that the paper under discussion establishes data and methods of sufficient accuracy for design of 400-cycle a-c aircraft electric systems. However, it is desirable that additional test work be conducted to determine with greater accuracy the value of r_g and x_g which were extended from limited test data by analytical methods. Such tests are essential, if circuits are to be placed in odd-shaped structures.

It would seem that the discussion by Mr. Le voy is inconsistent in that he wants to accept comparatively large errors introduced by suggested approximations but objects to small test errors of which we are aware and because of which we have suggested additional test work.

It is agreed that the a-c resistance should be slightly higher than the d-c resistance, but we are quoting field-test values and feel that they are well within the limits of errors which can be expected. Either the d-c or a-c value can be slightly in error.

Slight variations in reactance values also can be expected under the original test conditions.

The connections for all measurements were made by carefully cleaning the wire and the aluminum skin near structural members and clamping under high pressure with *C* clamps. Of course, contact resistance is included. However, it is considered negligible, as other tests indicate that loose-contact resistances between copper wire and aluminum may be as low as 0.07 ohm where no pressure is applied.

Higher-Voltage D-C Aircraft Electric Systems

Discussion and authors' closure of paper 44-238 by W. L. Berry and J. P. Dallas, presented at the AIEE Los Angeles technical meeting, Los Angeles, Calif., August 29–September 1, 1944, and published in AIEE TRANSACTIONS, 1944, November section, pages 843–9.

W. A. Barden (major, United States Army, Wright Field, Dayton, Ohio): The authors have presented an excellent case on behalf of 115-volt d-c electric systems for aircraft. It is the purpose of this discussion to point out a few considerations which, it is believed, are of importance in military aircraft, if not in commercial aircraft.

In general, the reasoning and the conclusions reached thereby are difficult if not impossible to contradict. Certainly there can be no argument as to the relative weights of conductor required for a given system operating at 115 volts d-c or 208/120 volts a-c current. However, the extra weight of wire in the three-phase a-c system makes a definite contribution from the standpoint of reliability. For example, in a single-wire d-c system only one wire need be broken in order to lose a given circuit. On the other hand, in the case of the three-phase a-c system, even in the event of a single circuit supplying a load, little trouble is caused by the loss of one wire in that circuit and in many cases two of the three-phase leads may be lost without losing operation of the equipment involved. This is considered to be a definite factor in the reliability of the electric system. It may be argued that there is less probability of one wire being damaged by gunfire or flak than there is in the case of three wires. However, each of the three wires is smaller in diameter for a given power down to the minimum size used than the one wire required for the 115-volt d-c system.

It is pointed out that circuit protection is complicated threefold in the three-phase a-c system over that required for a single-wire d-c system. However, the fuses for the three-phase system are designed as three-phase units so that the actual number of parts in this connection is no greater than that required for the equivalent single-wire d-c system. Furthermore, it can be expected that fuses for 120-volt d-c service will be considerably more difficult to design and produce and probably will be as heavy as the three-phase counterpart in the a-c system.

It is difficult to understand why design and manufacturing personnel would have to be especially trained for the safe operation of a-c aircraft systems. It is admitted readily that service personnel will have to be more carefully selected than in the past, but such is already the case with existing d-c systems on aircraft. The *B*-19 is still flying with very few difficulties due to the a-c electric system.

Mention is made of a line of three-phase 208-volt a-c relays being used for 115-volt d-c service having ratings up to five-kilowatt continuous duty and 20-kw rupturing capacity on this system. These same relays, when used on the three-phase 208-volt a-c system, have ratings up to 27-kw continuous duty and 108-kw rupturing capacity.

In regard to motors, the following comments are offered. While it is true that higher torque may be obtained with less weight in a d-c motor than in an a-c motor there is little else to recommend the former. Certainly, from the standpoint of reliability and upkeep required, there can be no comparison between d-c and a-c motors. Actually the three-phase induction motor represents the ultimate in reliability and simplicity of upkeep.

The paper raises grave doubts as to the practicability of constant-speed drives because of their weight and the hazards and complications which it is claimed accompany the use of such devices. This writer is at a loss to understand why the use of a hydraulic transmission which is self-contained entails unknown hazards and complications.

The heart of the constant-speed drive is no more complicated and certainly no less reliable than the pump which keeps the aircraft engines properly lubricated.

A question is raised as to the amount of synchronizing equipment required to make possible parallel operation of alternators. Actually, the alternators in question have been driven directly connected to aircraft engines of 450-horsepower capacity and have been connected together indiscriminately without attempting to carefully synchronize the machines. This has been done, not once, but many times under conditions of load and speed differences which are unbelievable to any but those who were present at the tests. Examples such as this would seem to indicate that synchronizing equipment is unnecessary.

Satisfactory parallel operation of alternators depends on governor control of the constant-speed drives. Surprisingly enough, that component of the governor which recognizes the need for a change in output of a given alternator is no more complicated than that component of a voltage regulator on a d-c system whose function it is to see that the d-c generator carries its proper share of load current.

The reactive-load-balance circuit, which is also necessary to satisfactorily parallel operation of alternators, is simplicity itself and the heart of this system is a current transformer no bigger than a doughnut.

The writers of the paper under discussion apparently feel that it is better to use six units of 15-kilowatt capacity than to use a smaller number of more efficient generators of greater output having a better ratio of output to weight. Not only does the former system have the disadvantages pointed out but voltage regulators, reverse-current relays, differential relays, and so forth are multiplied in proportion.

Reference is made to Table I of AIEE technical paper number 44-190[1] which gives a very enlightening picture of the comparative weights of generating and main distribution equipment for systems of 120-kw capacity using 28-volt direct current, 120-volt direct current, and 208/120-volt alternating current. An examination of this table shows that the weight of the overall generating equipment for a 208/120-volt a-c system is greater than for either of the d-c systems. However, when other components of the system are considered, it is seen that the a-c system is lighter than the 120-volt d-c system by a few pounds and that both of these systems are lighter than the 28-volt d-c system by approximately 400 pounds. Also, it is interesting to note that the weight of main power wiring for the 208/120-volt a-c system is only ten pounds heavier than for the 120-volt d-c system.

In conclusion, it is conceded that the same expenditure of time and money on a 120-volt d-c system as has been put into the 208/120-volt a-c system probably would have seen a 120-volt d-c system all set to go today. However, the a-c system, for all the new equipment which has to be devised, is on the verge of actual readiness at this moment.

It is believed that all will agree that at the time the active development of the 208/120-volt a-c system was undertaken the practicability of high-voltage d-c systems for high-altitude work did not seem quite so promising as it does at present. Hence, the decision was made to develop the equipment necessary to produce a wholly reliable a-c system for large aircraft and it still remains to be seen whether one system is more practicable than another for very-high-altitude service.

Reference

1. A 40-Kva 400-Cycle Aircraft Alternator, H. E. Keneipp, C. G. Veinott, AIEE Transactions, volume 63, 1944, November section, pages 816–20.

Harvey J. Finison (Aircraft Laboratory, Wright Field, Dayton, Ohio): In addition to the factors of reliability and weight as determinants of aircraft electrical system design, there are other equally important considerations:

1. Ease of operation.
2. Ease of maintenance.

Boice and Levoy[1] list a number of additional considerations.

Problems of operation and maintenance increase tremendously with the complexity of the system. Can any electric-power-supply system be simpler than the single-wire d-c system with ground return?

Where extreme importance is associated with a particular function, two- or three-channel service may be essential. For the d-c system, three-channel service can be obtained by three cables which are not required to terminate in any particular order at either the load or supply end. It would appear that similar reliability of service can be obtained with only two channels of three-phase a-c service consisting of six cables which must terminate in exact positions at both the supply and load ends. In the initial work on a-c systems, it is generally agreed that parallel operation of alternators is essential to the achievement of a reliable and invulnerable system. Present difficulties with incorrect connections, trouble shooting, and lack of understanding of paralleling adjustment procedures by operating and maintenance personnel on the d-c system cause some question among those who have been associated closely with operations in combat theaters as to the advisability of an a-c system.

While there can be little doubt that a complex electric system can be operated and maintained, the qualifications of personnel and the necessary training time are both greater. In military service these factors have considerable disadvantage.

There is good reason to believe that better accuracy of load division among the generating units can be achieved with higher-voltage systems than is being achieved on present 30-volt d-c systems. Higher equalizing voltages can be used with correspondingly less effect from contact drops in the equalizing circuit and less system-voltage droop with loss of a generator.

Problems of stability are probably comparable with both a-c and d-c systems. They have been very troublesome on d-c systems. We have taken oscillographs of 30-volt electric systems both in the laboratory and in flight where transfer currents among generators oscillated between positive and negative values in excess of the generator-current rating. Yet, while generator relays lasted only a few hours, the operation of system-load items still carried on in almost a normal manner. While the experience gained may make possible prevention of instability on the a-c system, if it is encountered, it can easily result in rendering the entire system inoperative as the alternators pull out of synchronism.

The tremendous impetus which has been given to development of 400-cycle three-phase a-c systems has caused some difficulty in interesting manufacturers in development of low-voltage and high-voltage d-c equipment. Many point to the history of development of electric public utility systems and conclude that a similar painful process of conversion from direct current to alternating current should be avoided by immediate adoption of a-c systems. However, there is not a direct comparison in that the aircraft electric system is an isolated plant covering a comparatively small area. There are no problems of long transmission lines requiring a voltage far higher than the maximum possible utilization voltage.

The maximum measured electrical load on any military airplane in present operation does not exceed 25 kw. The estimated load on that airplane was 45 kw. If the policy of installation of 100 per cent standby capacity had been followed, based on the load estimate, the generating capacity would have been 90 kw—to supply an actual 25-kw load. Aircraft designers should study load complements of new aircraft carefully, especially for transport and commercial types, when compared with latest operating military aircraft.

Some members of the aircraft industry have formulated conclusions on the a-c–d-c question similar to these:

1. Low-voltage d-c systems can be used advantageously for a number of postwar years due to availability of proved equipment and experience and lack of electrical loads exceeding those of present military aircraft.
2. Equipment manufacturers can develop equipment for 30-volt d-c systems with full confidence that it will not immediately become obsolete.
3. Higher-voltage d-c and a-c systems and equipment should both be developed with equal intensity until there is definite experience to judge their relative merits before deciding which has the greater advantage for future application.

Reference

1. Basic Considerations in Selection of Electric Systems for Large Aircraft, W. K. Boice, L. G. Levoy, Jr. AIEE Transactions, volume 63, 1944, June section, pages 279–87.

W. L. Berry and **J. P. Dallas:** At the Los Angeles technical meeting of the AIEE several comments were made to the effect that higher-voltage d-c power transmission may be lighter than a-c for medium- or low-power applications, but for military aircraft with high-power requirements, such as gun turrets, three-phase a-c power is lighter. This is not true. The weight for transmitting ten horsepower 100 feet over a single-wire 230-volt d-c system allowing five per cent voltage drop is two pounds, however six pounds of cable are required with the 208-volt three-phase grounded neutral a-c system assuming a power factor of 70 per cent. For higher power or longer transmission distances, the weight of the wiring for alternating current is more than 3 times that for direct current. For a more complete comparison refer to Figures 1, 2, and 3.

There are many factors which must be considered in selecting an airplane electric-

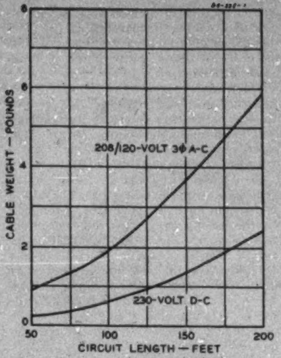

Figure 1. Aircraft cable weight for transmitting three horsepower continuously over single-wire grounded-return d-c systems and 400-cycle three-phase three-wire grounded neutral a-c systems operating with a power factor of 70 per cent

system voltage and the result must be a compromise. For distances greater than 50 feet and loads of one horsepower or more, 230-volt d-c wiring is lighter. Recent experimental data prove that 230-volt d-c aircraft switching is not difficult. The AIEE paper by J. S. Quill and L. T. Rader is an excellent discussion of the d-c switching problem.[1] Equipment is immediately available for 115-volt d-c applications, while for 230-volt direct current considerable developmental work may be required. Difficulties arise in the design of lighting equipment when voltage is increased. Hazard to personnel is least with 115-volt direct current, greater with 230-volt direct current, greater yet with 120-volt 400-cycle alternating current, and very serious with 208-volt 400-cycle alternating current.[2]

It was also stated that wiring constitutes a rather small percentage of the total weight of aircraft electric systems. Weight percentages are misleading, it is the actual difference of weight in pounds that counts. On a 150,000-pound airplane the higher-voltage d-c distribution system may be 200 to 600 pounds lighter than a comparable three-phase a-c system, depending upon the degree of electrification.

Recent tests have demonstrated beyond doubt that under certain rare conditions an arc will reverse direction in weak magnetic fields and reduced atmospheric pressure. Hundreds of tests were conducted on many devices of practical design embodying magnetic-arc suppression without once observing arc reversal. A special test contrivance proved that conditions of low field intensity and small gaps were required for arc reversal. A condition exists where the arc, although in a magnetic field, does not appear to move in either the normal or reversed direction. It is this condition which causes erratic or unsatisfactory arc interruption, however it is relatively easy to avoid in practical designs.

In reply to Major Barden's comment, it is not proper to consider the three wires of the a-c system more reliable than the single wire of the d-c system. If a three-phase motor is designed with the customary aircraft margin of safety, it certainly will not operate with the loss of one or two of the phases. The probability of loss of one of the three a-c phases is three times that for the loss of the one d-c conductor. For increased reliability of the d-c system it may be desirable to utilize three or more conductors in parallel. The loss of one of these parallel conductors will not seriously affect circuit operation. Also, parallel conductors frequently result in an over-all saving of conductor weight.

Fuse manufacturers state that the weight of a fuse for 120-volt direct current is no greater than for 208-volt alternating current. A three-phase fuse would be nearly three times as heavy as a d-c unit and would, of course, have many more components resulting in a device having six terminals which require proper interconnection instead of two terminals.

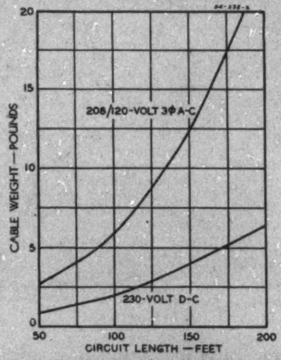

Figure 2. Aircraft cable weight for transmitting ten horsepower continuously over single-wire grounded-return d-c systems and 400-cycle three-phase three-wire grounded-neutral a-c systems operating with a power factor of 70 per cent

There is no doubt that the added complexity of the a-c system creates a need for technically informed personnel for design, installation, testing, and maintenance just as it does for proper operation.

The three-phase relays were mentioned to show that relays are at present available for immediate d-c service up to five-kw continuous duty and 20-kw rupturing capacity. Relays specifically designed for 115- or 230-volt direct current can be considerably lighter than for three-phase alternating current considering equal power ratings.

For intermittent-duty motors it is likely that 1,000-hours brush life will correspond to the life of the airplane. For high-output or continuous-duty motors, 1,000-hours brush life would correspond to a convenient inspection and overhaul period. It should be noted that for certain motor characteristics a-c motors will incorporate slip rings and brushes which have comparable life to d-c commutators and brushes.

The hydraulic transmission is only one type of several proposed constant-speed drives. One by one these drives are being discarded either because of failure, complexity, inefficiency, or excessive weight. To date there is no constant-speed drive to our knowledge which has proved completely satisfactory as the result of actual tests.

It is hoped sincerely that the enormous amount of synchronizing equipment such as was required for the B-19 three-phase a-c electric system can be eliminated as suggested by Major Barden.

The tolerances for adequate load sharing of d-c generators are exceedingly wide compared to those for proper synchronization of alternators. Major Barden speaks in vague terms of the voltage- and speed-sensing elements of a-c and d-c systems. Such comparisons are very confusing and mean less than nothing. We will be very surprised if the entire governor system of any a-c system will compare in weight and simplicity to the six-pound units to be used with present d-c equipment. It is the complete system and not a single component thereof which should constitute the basis for comparison.

A wrong impression has been formed; the authors were merely indicating that for a given requirement, six engine-driven 15-kw d-c generators would be more reliable and lighter than two 60-kva alternators driven through constant-speed drives by the main engines. The 15-kw generators were used in the example because they could be directly mounted on existing engine drives while larger capacity generators equivalent to the a-c machines would have required remote-drive mechanisms which impose large weight penalties. If and when main engines are equipped with sufficiently large generator drives, additional weight savings

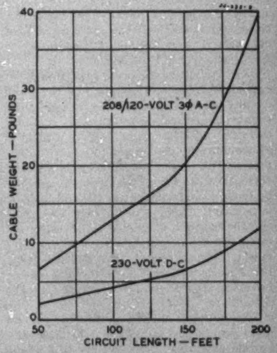

Figure 3. Aircraft cable weight for transmitting 20 horsepower continuously over single-wire grounded-return d-c systems and 400-cycle three-phase three-wire grounded neutral a-c systems operating with a power factor of 70 per cent

and equipment reductions can be effected for d-c systems.

References

1. D-C Arc Interruption for Aircraft, J. S. Quill, L. T. Rader. AIEE Transactions, volume 63, 1944, December section, pages 883–8.

2. Effect of Frequency on Let-Go Currents, C. F. Dalziel, Eric Ogden, Curtis Abbott. AIEE Transactions, volume 62, 1943, pages 745–50.

Vacuum-Tube Radio-Frequency Generator—Characteristics and Application to Induction-Heating Problems

Discussion and author's closure of paper 44-236 by T. P. Kinn, presented at the AIEE Los Angeles technical meeting, Los Angeles, Calif., August 29–September 1, 1944, and published in AIEE TRANSACTIONS, 1944, pages 1290–1303.

J. Elmer Housley (Aluminum Company of America, Alcoa, Tenn.): The author's use of power terms in his discussion of the radio-frequency circuits makes the article valuable to electrical engineers.

The article is a valuable reference for those concerned with the application of a commercial radio-frequency generator to some particular heating problem.

The article centers around the tuned circuit and methods of coupling to the work circuit. A very detailed description of the tuning condenser with regard to size and dielectric losses is given, but very little is said about the associated tube and inductance. The author says, "the larger the capacitor C, the more stored energy and the more circulating current in the coil." This is true, but at high currents the resistance and the reflected resistance of the coil enters more and more into the circuit to dissipate power. In other words, it would seem that the *kva/kw* ratio or Q of the coil would be more critical than that of the tuning capacitor.

The author's discussion of the *kva/kw* ratio should be amplified. In the discussion he states: "To produce this condition, the *kva/kw* ratio must be kept high." The author does not state clearly the condition, nor does he define the *kva* or *kw*. The *kva* can be taken to apply to the circulating current in the tank circuit, but does the *kw* apply to power stored in the L-C circuit, power dissipated in the L-C circuit, or power available in the work circuit?

The author's mathematical examples deal with calculation of the work coil and should serve well as a reference for those faced with this problem.

It would be advantageous to have information on the shielding of these radio-frequency generators for the elimination of radio interference to aircraft and marine services on the fundamental frequencies and on broadcast and other services on harmonic frequencies.

T. P. Kinn: The comments by Mr. Housley are well taken. The discussion on *kva/kw* ratio throughout the paper possibly can be better understood, if it is kept in mind that, always, reference is being made to the "working" *kva/kw* ratio of the L-C circuit. By this is meant the ratio of *kva* in the circuit to the *kw* being supplied by the circuit. This *kw* is made up of the power being supplied to the load and the power being dissipated in the capacitor and inductance of the circuit. The Q or *kva/kw* ratio of the capacitor and inductance is important only in determining the power being dissipated in these two elements of the circuit. The majority of the power lost in the L-C circuit is dissipated in the inductance, and normal design dictates that the Q of the coil be as high as possible to keep this loss to a minimum. As pointed out in the paper the loss in the L-C circuit goes up as the square of current, and therefore the value of inductance and capacity is kept at a point where this loss will not be excessive (a *kva/kw* ratio of 50 was selected in the paper as the desirable design point).

Shielding of the radio-frequency generator and in most cases the work circuit is necessary to insure that radiation is kept to a minimum. Good engineering practice requires that the generator be housed in a metal cabinet capable of completely shielding the radio-frequency circuits. Well designed radio-frequency filters are necessary to eliminate possible radio-frequency radiation through the power circuits. The work coil and connection leads should be shielded as thoroughly as possible to reduce radiation from that source. Induction-heating work circuits are inherently poor radiators, but good engineering practice again dictates that every precaution be taken by shielding wherever possible.

Electrical Control in Automatic Pilots

Discussion and authors' closure of paper 44-187 by C. M. Young, E. E. Lynch, and E. R. Boynton, presented at the AIEE Los Angeles technical meeting, Los Angeles, Calif., August 29–September 1, 1944, and published in AIEE TRANSACTIONS, 1944, December section, pages 939–43.

W. A. Barden (major, United States Army, Wright Field, Dayton, Ohio): This paper has given a very clear-cut understandable picture of the components of automatic pilots and the functions of each. It points out the means by which the deviation of an aircraft from its selected course is recognized and gives in detail the chain of events by which that condition is corrected.

As the paper shows, not only is flight control accomplished automatically by means of electric equipment, but many functions having to do with the proper operation of the engines are being controlled in the same way as well. Since automatic control is finding its way into every phase of flight operation, it would seem only logical that certain components of these systems could be standardized. For instance the electric pick-off devices could be standardized for the detection of any number of conditions requiring corrective action in order to insure the optimum performance of the aircraft. In the same way the amplifiers and servo units could be made standard so that any device could be used in conjunction with the other devices to result in an automatic control system. The Army Air Forces already has initiated a program to accomplish this result.

The automatic pilots described in the paper are for use in keeping the aircraft on course and for mild maneuvers. However, by the use of servo units of sufficient power and follow-up mechanisms of sufficient accuracy a system could be devised whereby the aircraft could be flown in all sorts of maneuvers simply by the manipulation of a device which would send the proper signals to the amplifier and thence to the servo units which actually move the control surface.

Some airplanes of the future will be too large to be handled in any other way. Therefore, why not use a system of electric pick-off devices, amplifiers, and servo units which are capable of controlling the flight of the airplane in response to automatic signals or directions from the human pilot?

This would involve a slight increase in the weight of servo units but would at the same time eliminate very bulky and heavy control columns.

It is believed that an a-c electric system supplying power to servo units consisting of induction motors coupled to control surfaces through variable-ratio torque/speed converters would offer the ultimate in reliability and speed of response.

C. M. Young, E. E. Lynch, and E. R. Boynton: The discussion by Major Barden presents some very interesting possibilities in the field of automatic control in aircraft.

The standardization of system components is a worthy objective. As he has pointed out, there is a definite trend toward automatic engine control, automatic flight control, and interconnection between these two systems so that standardization of components would be beneficial. It is desirable, however, that this standardization should be made only on components which have been well established in the art so that the course of future developments will not be jeopardized.

As the control circuits are made more comprehensive and with the advent of instruments capable of producing a signal corresponding to any attitude of the plane about each of the three axes, complete maneuvering of the plane in any attitude will become a reality.

In this system it will be possible to make use of the servo units as power-boost units so that the human pilot will be able to control the airplane manually through the autopilot servo units with a minimum of fatigue. Ultimately, this type of control, particularly on very large airplanes, probably will replace completely the conventional cable systems. To achieve complete replacement, however, all of the units involved in the servo system must be as reliable as the present direct mechanical drive. Once this objective is achieved, there should be no difficulty in overcoming a pilot's or passenger's instinctive aversion to this type of control.

There is no doubt that the standardization program now being undertaken by Wright Field will result in close specifications and rigid control of material and workmanship. As a result this should go a long way toward increasing the reliability of units. The servo design itself probably will be increased considerably in reliability through the use of squirrel-cage induction motors with their inherent ruggedness and simplicity of design. Once comparative reliability is achieved, completely automatic flight will become a reality.

Electric Automatic Pilots for Aircraft

Discussion of paper 44-179 by P. Halpert and O. E. Esval, presented at the AIEE Los Angeles technical meeting, Los Angeles, Calif., August 29–September 1, 1944, and published in AIEE TRANSACTIONS, 1944, November section, pages 861–6.

B. Levine (nonmember; Wright Field, Dayton, Ohio): This paper presents an excellent description of rate-type control as applied to automatic control of aircraft. The *A*-5 automatic pilot, about which the discussion presented in this paper was centered, was the first electric automatic pilot in this country utilizing this principle of control. Rate control has been attained in the past to a certain extent mechanically, or pneumatically; however, satisfactory operation is difficult to achieve due to the limitations and complexity of this type of rate follow-up system. This system is being utilized in this country in the type *A*-4 automatic pilot. A three-axis electric automatic pilot has been developed and is in use in Germany utilizing the rate principle of control. In this case, however, rate control is achieved through the use of three-rate gyroscopes whose signals are blended into the basic displacement signal electrically.

There are both advantages and disadvantages to obtaining precision control through a rate system. The principal advantage of a system as described in this paper is that control is achieved to the same degree of accuracy under a wide variation in airspeed. This is of considerable help in considering the problem of automatic landing and take-off wherein the automatic-pilot servo system may act as a boost control. Some of the problems which are encountered in use of a control system of this nature are:

1. It is difficult to shock mount the gyro controls as the vibration of the units introduce a rate kick into the control system with resultant erratic operation. This means that the controls must be bolted solidly to the aircraft structure, thus necessitating a more rugged and heavier construction.

2. A very stable power source is required to obtain satisfactory operation with fairly close frequency and voltage tolerances. Cyclical loading of the power supply is apt to cause interference with the pilot's operation, and modulation of the power supply between zero cycles and the natural frequency of the aircraft cannot be tolerated.

3. Because of the greater preciseness of control obtainable through a rate pilot than through the conventional displacement pilot, an educational program is necessary to educate human pilots in the differences in the type of control obtainable from the two systems.

In large-quantity aircraft production tolerances are not held as closely as may otherwise be possible. As a result, considerable control-cable friction variation is often present with a consequent decrease in tightness of control. This possibly may be eliminated to a certain extent through the use of position repeat-back-type servo units in place of force repeat-back servo units.

Application of Electronics to Aircraft Flight Control

Discussion of paper 44-239 by W. H. Gille and R. J. Kutzler, presented at the AIEE Los Angeles technical meeting, Los Angeles, Calif., August 29–September 1, 1944, and published in AIEE TRANSACTIONS, 1944, November section, pages 849–53.

Vincent H. Quayle (nonmember; lieutenant, United States Army Air Corps, Wright Field, Dayton, Ohio): All phases of science have made great advances as a result of the war, and among them is the comparatively new science of electronics. Electronics as applied to aircraft, excluding radio communication, includes turbo-regulator control and the automatic pilot.

The heart of any electronic device is the vacuum tube. During the past, vacuum tubes, used for aircraft radio communication, could fail without any serious consequences other than loss of communication. With the application of vacuum tubes used to control turbosuperchargers, automatic pilots, and so forth, the situation is completely reversed. Any failures easily could result in the loss of a valuable airplane, including its highly trained crew.

In the interest of safety for all concerned, it is imperative that the quality of vacuum tubes used in aircraft be improved and maintained up to the highest possible standard. Many of these tubes are an outgrowth of those originally designed for home and automobile radios and are given the minimum of factory tests. Tubes to be used in aircraft must be subjected to more rigid tests for life, heat, cold, vibration, and altitude. These tests will be necessary to give electronic controls the reliability approaching that of an equivalent mechanical system.

Influence of Electricity on Aircraft Instrumentation

Discussion of paper 44-193 by C. F. Savage, presented at the AIEE Los Angeles technical meeting, Los Angeles, Calif., August 29–September 1, 1944, and published in AIEE TRANSACTIONS, 1944, November section, pages 802–05.

W. A. Barden (major, United States Army, Wright Field, Dayton, Ohio): This paper provides an interesting account of the way in which instrumentation is accomplished electrically in modern aircraft. Although instruments operating on electrical principles have been developed to meet every need that exists, there still is a considerable amount of work which could be done in the way of improvements and refinements.

These improvements and refinements well could be in the way of increased accuracy and rate of response. Possibly both could be achieved as a result of improvements in small precision bearings, greater accuracy in machining rotor shafts, laminations, and so forth.

Other fields of development open to instrument engineers might be in reduced size and weight of the instruments, improved efficiency, and power factor. Great strides already have been made in this respect.

Instruments have been developed for use on 208/120-volt three-phase 400-cycle systems which are unbelievably small and light. Each of the instruments has been incorporated into the two and one quarter-inch *AN* case. The 0–250-volt voltmeter and the 0–250-ampere ammeter weigh approximately three fourths of a pound each. The wattmeter having a range of 0–50 kilowatts, and the frequency meter having a range of 350–450 cycles per second, each shows a weight of less than one pound.

Each of these instruments has an accuracy of plus or minus two per cent of full-scale reading and is capable of withstanding vibration and other conditions incident to its use in aircraft.

Instrumentation of 400-Cycle Aircraft Electric Systems

Discussion and authors' closure of paper 44-240 by A. J. Corson, A. G. Stimson, W. A. Soley, presented at the AIEE Los Angeles technical meeting, Los Angeles, Calif., August 29–September 1, 1944, and published in AIEE TRANSACTIONS, 1944, November section, pages 830–5.

J. E. Rowland (nonmember; Wright Field, Dayton, Ohio): The design, characteristics, and performance of 400-cycle instruments have been well covered by the authors in their very interesting paper. There is little that can be added to their presentation.

The instruments promise to be an excellent solution to the problem of instrumentation of 400-cycle aircraft electric systems. In size and weight they are all that can be desired. They are small and compact and yet the scale can be easily read. On the whole, they have satisfactorily withstood the laboratory tests, all of which are nearly completed at the present time. However, the tests have brought out the following minor faults:

A. Accuracy on the minor scale divisions is not within two per cent of full scale value. This fault obviously can be easily eliminated by a more accurate location of these scale divisions.

B. The self-heating error of the frequency meter is appreciable. This instrument must be in operation approximately 30 minutes before it will indicate with maximum accuracy. Although this fault is not serious, it should be eliminated, if possible, from an instrument of such excellent design otherwise.

C. The instrument cases are not dust-tight or moistureproof. It may be difficult to make them moistureproof without hermetically sealing them, which may not be such a bad idea at that. But, at least, by using suitable gaskets, they can be made dust-tight.

The instruments have yet to undergo the final and conclusive test. This test means subjection to actual operating conditions in aircraft. These operating conditions are very severe and may be summarized as follows:

A. Temperature. A temperature range of from −55 degrees centigrade (−67 degrees Fahrenheit) to +70 degrees centigrade (+158 degrees Fahrenheit).

B. Altitude. An altitude pressure range of from sea level to 55,000 feet.

C. Humidity. A relative humidity from 0.5 to 100 per cent.

D. Dust. Exposure to airborne sand and dust particles encountered on deserts.

E. Vibration. Conditions of linear and torsional vibration incident to normal continuous use in aircraft.

A. J. Corson, A. G. Stimson, and W. A. Soley: Mr. Rowland's discussion appears to be the result of careful analysis based on a thorough knowledge of the application of 400-cycle electric aircraft instruments.

The self-heating error of the frequency meter is due to confining the circuit elements to minimum space. The moderate temperature rise due to self-heating slightly changes the circuit-constant relationships. The error is calibrated out by marking the scale after the instrument has been operating for some time. Starting cold, the frequency indication should not change more than one cycle due to self-heating after the first five minutes of operation. Considering the usual warming up period for an airplane, the initial self-heating error of the frequency meter would seem to be of no consequence during flight.

The instruments can be made dust-tight with suitable sealing compounds, but it is recognized that hermetic sealing is preferable for protection against fungus growth and other tropical conditions.

The Gyrosyn Compass

Discussion and author's closure of paper 44-181 by O. E. Esval, presented at the AIEE Los Angeles technical meeting, Los Angeles, Calif., August 29–September 1, 1944, and published in AIEE TRANSACTIONS, 1944, November section, pages 857–60.

Arthur Wahl (lieutenant, United States Army Air Corps, Wright Field, Dayton, Ohio): The primary purpose of the Gyrosyn compass described in this paper is to obtain a stable and accurate indication of azimuth under a wide range of flying conditions and to have this indication as free as possible from the deviations and fluctuations inherent in the direct-reading magnetic card-type of compass. The method of arriving at such an indication by using the earth inductor (called flux valve in this paper) to sense direction with respect to the earth's magnetic field at present can be divided into two classes: Either the earth inductor is allowed to swing pendulously and is made to slave a horizontal axis gyroscope used as the indicator; or the earth inductor itself is stabilized in the horizontal plane by a vertical axis gyroscope, and its signal of direction is amplified through a servo system to give the desired indication. The first of these two, which is employed in the Gyrosyn compass, has been dealt with ably in this paper. The second, which is employed in the Gyro Flux Gate compass built by Eclipse-Pioneer division of Bendix Aviation Corporation, will be described briefly in principle of operation with a short comparison of the two systems.

The earth inductor, which contains a permeable-alloy core which begins to saturate at a low flux density, readily lends itself to be the source of the desired compass indication, because it can be placed in a remote location from local magnetic disturbances inside the aircraft and because it does not itself turn in a horizontal plane with respect to the aircraft as must the needle of a card-indicating type of compass. However, when the earth inductor is tilted out of the horizontal plane, the vertical component of the earth's field causes it to give a false indication of azimuth, and the degree of this false indication is a function of the angle which the earth inductor makes with the horizontal plane. To give the desired accurate and stable direction indications itself, the earth inductor must be stabilized in the horizonal plane regardless of the attitude of the aircraft. It is then sensitive only to the changes in azimuth as desired. When the servo mechanism into which it feeds has a follow-up rate which greatly exceeds any possible rate of turn of the aircraft, the system functions as a deadbeat azimuth indicator.

If the earth inductor is pendulously mounted, the random pitches, yaws, and rolls of the aircraft cause it to swing out of the horizontal plane and thereby cause its instantaneous signals of direction to be false. However these swings generally will be about a mean axis which is the desired true vertical. The mean of the false direction signals then will be the true direction signal, so that when the relatively fast-swinging earth inductor slaves a device such as a horizontal axis gyroscope, which cannot follow its instantaneous signals, the gyroscope will be slaved to the mean or true azimuth direction.

Both of these systems have advantages and disadvantages. Both of them begin to give false indication of direction in long-persisted turns because they both depend upon gravity to define the horizontal plane, and in turns the centrifugal acceleration of the turn combines with true gravity to give a false-gravity direction. (Uncoupling the directional gyroscope from the earth inductor in long turns might not be satisfactory, because the turbulence of flying may cause the free gyroscope to wander and actually to be more in error than it would be, if it had remained coupled to the earth inductor.)

At present the pendulously mounted earth-inductor system slaving the directional gyroscope seems to have the advantage of space, weight, and complication over the gyro-stabilized earth-inductor system, but this advantage is not necessarily inherent. The accurate compass indicator is being called upon more and more to furnish azimuth signals to navigation, radio, armament, and photographic equipment. The loading of these signal sources on the directional gyroscope may cause it to become sluggish and inaccurate itself, unless these signals are produced through an additional servo system; whereas the servo unit for the original indication from the gyro-stabilized earth inductor system may be made powerful enough to operate any such combination. Furthermore, the latter system lends itself more readily to arbitrary mechanical compensation to make the final indication right, and this is often necessary because magnetic disturbances and inherent transmission errors do not often combine to make a simple sine or double-sine curve of compensation adequate.

On the other hand the problem of maintaining a vertical-axis gyroscope at a remote part of the aircraft, where it is subjected to severe vibrations and accelerations, and of still keeping the unit as small, light, and rugged as possible is a difficult one; whereas the pendulously mounted earth-inductor unit is inherently smaller and more rugged. Furthermore, a relatively poor directional gyroscope could be slaved by a good earth inductor and result in a relatively good compass system.

The Gyro Flux Gate compass has been in use by the Army Air Forces for some time, and its merits and limitations are quite well known by this time. The Gyrosyn compass is relatively new and is still undergoing performance tests by the Matériel Command.

C. F. Fragola (for the author): Lieutenant Wahl's discussion of the paper on the Gyrosyn compass is a definite contribution to the field of remote-indicating compasses, primarily because of his development of the fundamental differences between two basic methods of achieving stabilization. It is expedient that the following comments be added for completeness.

He has mentioned that the Gyrosyn compass might not be satisfactory in long turns, because of the turbulence of flying, to uncouple the directional gyroscope from the earth inductor because the error encountered due to free wander of the gyroscope might be greater than would have resulted had it remained coupled to the earth inductor. In practice the normal directional gyroscopes do not have drifts exceeding 20 degrees an hour, and many have much lower drift rates; however, assuming 20 degrees per hour, then in a standard-rate 180-degrees-per-minute turn the maximum gyro drift would be one third of a degree, which is negligible. Mention also is made of the possible limitation of using repeaters coupled to the azimuth gyroscope to supply azimuth signal to navigation, radio, armament, and photographic equipment. In practice six repeaters have been used satisfactorily when electromagnetically coupled to the azimuth gyroscope. If too many repeaters are mechanically or electromagnetically coupled to the gyroscope, the only effect would be that torques would be reflected back to it. This might necessitate a higher leveling torque, which is undesirable. If the torques reflected to the gyroscope become excessive in an installation where more than six repeaters are used, it is always possible to have an amplifier interposed between the gyro unit and the repeaters, thereby isolating the reflected torques. Then in an installation requiring less than six no weight burden is imposed.

Lieutenant Wahl advances the opinion that a mechanical compensator seems to be adapted more readily to the compass having the earth inductor stabilized by a gyroscope. To compare accurately the relative merits of a mechanical and permanent-magnet compensator it is well to remember that in a good compass the major error should be attributed to the magnetic deviation which exists at the point of location of the inductor unit in the aircraft. In the Gyrosyn, because the inductor unit has been designed to be located remote from most deviations, any residual deviation should be of low magnitude. In an aircraft under the specified conditions the magnetic deviation can be assumed to be entirely due to hard iron (permanent-magnet deviation), the soft iron effect (or induced magnetic effect) being negligible. If the hard-iron deviation is compensated by a permanent-magnet type, which in effect simply nullifies the error vector due to the hard iron, then this form of compensation will remain valid regardless of changes of the earth's field strength. A mechanical type of compensator in effect compensates for a certain error reading at the indicator for a given magnetic deviation. This error reading will be dependent on the strength of the earth's field; therefore, if the mechanical compensator is set in one field strength, it will be in error when used in another. In practice the magnetic compensator is far less complicated, more reliable, and can be applied in less time. If a compass system has objectionable fixed transmission errors, it may be necessary to use a mechanical compensator, especially when these errors are other than a single-cycle curve. In this case complete compensation would require a magnetic type in addition to the mechanical compensator.

Brief Survey of Power-Supply Developments on British Aircraft

Discussion of paper 44-217 by P. W. Carter, presented at the AIEE Los Angeles technical meeting, Los Angeles, Calif., August 29–September 1, 1944, and published in AIEE TRANSACTIONS, 1944, November section, pages 806–07.

W. A. Barden (major, United States Army, Wright Field, Dayton, Ohio): The paper which is the subject of this discussion was read with considerable interest and has resulted in a new appreciation of some of the problems faced by the British in using aircraft of their own and of American design.

This discussion will be confined to that part of the paper which deals with power supplies. It is well known that British and American answers to the questions involved have differed in many respects. However, loads now have grown to such an extent that it is believed the only satisfactory way of supplying a-c power for loads is by means of an engine-driven alternator. Such units are under development as have a rated output of eight kva, 115 volts, single phase, at rotor speeds of 4,000 to 8,000 rpm. Physically, the alternator will be very much like the present *P*-1 d-c generator and will weigh approximately 45 pounds.

It is believed that no attempt will be made to build a d-c generator into the same housing as the above described alternator because of the excessive length of such a unit as well as the large requirements for d-c power on the airplanes for which they are used. Further, if the a-c power on a given airplane has been supplied by inverters, one or more d-c generators can be replaced by alternators with no loss in net d-c power, but with appreciable saving in weight and gain in efficiency.

Numerous papers have been presented covering various aspects of the a-c power system under development in this country, so that comments on the system under development by the British would be somewhat superfluous in this discussion.

Mr. Carter is to be complimented upon his efforts in presenting an interesting history of the development of electric generating equipment in Great Britian.

Aircraft Electrical Horizons

Discussion of paper 44-218 by Frank W. Godsey, Jr., William L. Berry, and T. B. Holliday, presented at the AIEE Los Angeles technical meeting, Los Angeles, Calif., August 29–September 1, 1944, and published in AIEE TRANSACTIONS, 1944, November section, pages 821–4.

L. G. Levoy, Jr. (General Electric Company, Schenectady, N. Y.): The authors have presented an interesting glimpse of the future of aircraft electric systems. In setting the goal for electric systems in aircraft, it is not desirable to try to perform every function electrically, nor should we attempt to make the maximum possible use of electricity on aircraft. Rather, the goal should be to utilize electricity wherever it can perform the required function in a superior manner, compared with alternative means of accomplishing the same result, considering the airplane as a whole. The electrical industry must co-operate with all other industries in producing the best combinations of devices, as well as making electrical devices as good as possible.

This paper stresses the necessity for reliable apparatus, and this is certainly one of the most fundamental requirements. In addition to this, much can be accomplished in the way of increased reliability by improved system design. Control systems should be arranged to "fail safe" where possible. Communication of failure among systems should be minimized. Duplication of important functions can provide the increased reliability in cases where this is justified. Automatic transfer of power or signal channels can be provided as required.

The statement is made that, "If a perfect circuit breaker were developed, it would be designated by an American Wire Gauge number." Such a designation would be inadequate since a circuit-breaker rating must include such items as voltage rating, frequency (if a-c), interrupting ability, and many other items not inherent in a wire-size designation. A circuit breaker properly selected for a given piece of wire in one system may be entirely inadequate for that same piece of wire in another system, or at a different location in the same system.

The paper considers only thermal protection which aims to allow the apparatus to be stressed to limit. All other forms of failure protection, fault protection, overspeed protection, overvoltage protection, and so forth, aim to inactivate that unit at once when abnormal conditions are detected, or at least provide adequate warning.

Hermetically sealed switchgear has the many virtues ascribed to it; however, it introduces two problems to which the designer should give special attention. These are ease of inspection and maintenance in the field. Most contact-making devices have to be inspected periodically to insure continuous and reliable performance. One of the major problems in switchgear arises in the closing of a circuit, where under certain conditions the contacts have a tendency to stick or "weld." This problem is substantially independent of altitude and frequently governs the design of the switch. It is possible that a weight saving can be achieved by eliminating individual enclosures for each device and enclosing a group of devices in a single dust-tight enclosure in some applications where it is feasible to group the devices.

Better cooling of conductors may permit use of higher-current densities; however, this results in more loss and consequently poorer efficiency of the transmission system. This in turn results in a greater weight of gasoline burned in the engine to supply these losses. It is not desirable to push the current density beyond the point where the weight saving in conductor is more than offset by the additional weight of fuel burned. In some instances voltage regulation is a limiting factor also.

Wire sizes considerably smaller than *AN*-20 are of course used in aircraft apparatus. It is possible that, with improved wires and installation techniques, smaller wire sizes for aircraft wiring may be permissible in the future.

In the field of cockpit and instrument lighting, radio-active luminous and phosphorescent-luminescent markings have been supplemented by fluorescent-luminescent markings using ultraviolet light sources whose intensity are readily controlled either automatically or by the pilot. Judicious use of these various types of markings are very helpful in solving this most difficult lighting problem.

Prerotation of the wheels of a large airplane offers an additional advantage in reducing the stress on the landing gear which may result in substantial weight savings in some cases and may more than offset the weight of the prerotation equipment. The landing-gear stress is reduced, providing the speed matching between the periphery of the wheels and the runway is sufficiently close that any differential speed is eliminated during the interval of grazing contact, before very much weight comes on the wheels. This may be difficult to achieve consistently in practice. Among the disadvantages of prerotation are:

1. The problem of maintaining accurate wheel balance becomes more acute.
2. It increases slightly the amount of energy which must be absorbed in bringing the airplane to rest.
3. Increased complexity.

In connection with a-c, d-c, and hydraulic comparisons, the authors have pointed out that parallel operation of alternators is desirable and can be accomplished by a suitable speed and torque converter. Two other methods appear on the horizon:

1. The exhaust-gas turboalternator has ideal characteristics for parallel operation and high fuel economy. Other practical means for partial recovery of waste heat energy now discarded in the exhaust gases may appear.
2. The small auxiliary gas turbine using gasoline or other fuel has interesting possibilities.

In addition to the other advantages mentioned, a-c power systems minimize certain radio-interference and compass-interference problems.

On the horizon today are many other new and improved devices which offer tremendous fields for the application of electricity in aircraft. Among these are simplified and improved blind-landing systems, navigation, obstacle-detection, and other flying aids. The field of electric servomechanisms and remote positioning on large aircraft is rapidly developing. The marriage of electric, electronic, and hydraulic systems provides power package units and special control features which may find extensive use in aircraft. Electric- and hydraulic-power boost-systems will relieve the overworked muscles of pilots and permit them to handle the larger airplanes of tomorrow with ease. These devices will allow the aircraft designer more flexibility resulting in improved performance of the airplane.

Thyratron inverters and other electronic power and control systems will find their place in aircraft. In large complex airplanes, automatic reading and checking of instruments with a prearranged program will relieve the flight engineer of an onerous task. This can best be accomplished by electrical means. Lower battery weights are on the horizon, and improved system design may eventually pave the way for its complete elimination in many cases. Coordinated-system designs will provide improved reliability of the electric system and airplane as a whole. Air conditioning for passenger and crew comfort will be improved for aircraft. Refrigerating units for use in aircraft will be used for transportation of special products. Electricity will have an important place in these applications.

As electric systems in aircraft grow more complex, operating and service personnel must be trained so that there is no "mystery." We are confronted with an enormous problem of education which has not kept pace with the rapid development. Reliable apparatus must be properly installed, properly serviced, and properly operated, to produce the reliable results upon which public confidence is based. New or improved educational techniques must go hand in hand with our new developments, if we are to realize more fully the coveted goals of reliability and public confidence.

W. A. Barden (major, United States Army, Wright Field, Dayton, Ohio): Apparently the writers of this paper have been gazing into a crystal ball, or was it a periscope? Nevertheless the result is a stimulating paper which should go far in spurring technical talent toward the achievement of the goals which have been cited. It might be well for the chief electrical engineer of every organization interested in aviation electric equipment to brief his staff for a mission such as indicated.

In comparing the generating equipment for 230/115-volt d-c system with that of 208/120 volt a-c system, it has been pointed out that the constant-speed drive and generator for the alternating current undoubtedly would weigh more than for the d-c generator of equal rating. However, it must be remembered that the alternator is rated at a given speed which is at least two times the speed at which the d-c generator must develop the same amount of power. When considered on this basis, it is believed that the over-all weights of generating equipment will be equal, if not actually in favor of the alternator and its drive.

Since it is possible to build airplanes so large that human effort is inadequate to control them, the paper points out that power boost is needed. This writer feels that a better solution would be the use of entirely power-operated flight controls, leaving supervision to the human or automatic pilot. The reasons for this are as follows: With power boost overcontrol often results because of lag inherent in the boost system or the use of servomechanisms in the boost system which are incapable of moving the surfaces in response to the fastest rate required; thus the pilot tries to accomplish a given maneuver, the airplane does not respond as desired, and so the pilot continues to move the controls in a direction necessary to accomplish the desired result; before he realizes it, the airplane has attained an attitude which exceeds that which was desired, and he has to return the controls to a point which will give the necessary results. Whereas, if servomechanisms are used, capable of moving the surfaces, in accordance with any rate desired by the pilot, the largest airplane could be handled easily with no sign of overcontrol.

Incidentally, one gadget which would be a great boon to aircraft electric equipment is a variable-ratio torque–speed converter which could be installed on an induction motor in a manner similar to a gearbox. Such a device would eliminate the greatest handicap of induction motors, that is, the inability to attain variable-speed output.

Recent Developments in Light Sources

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Recent advances in the field of illumination before the war and the probable postwar developments and applications furnish the subject of this article. Arc, tungsten-filament incandescent, infrared, mercury-vapor, sodium-vapor, and fluorescent lamps are discussed in detail.

LIGHT is usually produced through the process of either incandescence (which for this general discussion may be taken to include combustion) or luminescence. Sources brought to a glowing temperature of incandescence are usually accompanied by infrared or heat radiation and very little ultraviolet radiation. Those that produce light through the process generally characterized as luminescence are electronic devices that may produce a good deal of ultraviolet as well as infrared radiation. Generally speaking, incandescent sources provide continuous spectra, whereas luminescent sources provide line or band spectra. Indications of several responses to radiation in the ultraviolet, visible, and infrared regions are set forth in Figure 1.

Radiation between wave lengths approximately 3,800 and 7,600 angstroms stimulates the human eye to vision. This narrow band of about one octave, out of about 80 octaves of the electromagnetic spectrum, is adjoined immediately by shorter wave lengths in the ultraviolet region that are applied in producing fluorescence and for photochemical and germicidal purposes. It is adjoined immediately by longer wave-length radiation that is utilized for drying and heat therapy.

It perhaps will not be out of place to recall further that the response of the human eye to radiation within the visible spectrum varies greatly for radiation of different wave lengths, attaining to a maximum value near the middle of the visible spectrum at about 5,550 angstroms with a shift of this maximum toward shorter wave lengths as intensity is decreased. The relative luminosity curve of normal human vision has been established by the International Commission on Illumination as in Figure 2.[1] These are the accepted data for the visual response of the human eye to radiant energy of various wave lengths.

Electric lamps are available in wide variety, having different qualities that fit them for application under different circumstances for several distinctive purposes. In some respects they may be likened to the instruments of an orchestra. Each has its place. All fulfill some useful purpose. The range of sounds of the orchestral instruments from the deepest "umph" of the contrabase tuba to the shrillest note of the piccolo is of the order of seven octaves. The composer calls upon them individually and in any combinations to produce desired effects. Electric lamps produce radiation of frequencies covering a range of like extent from the longest-wave infrared of glowing bodies to the shortest-wave ultraviolet radiation. The engineer may draw upon the resources of any one or of a combination of types of radiators, according to the purposes to be served.

Electric lamps that meet more or less successfully the requirements for radiation throughout the entire optical spectrum are commercially available. Indeed, many of these requirements may be served by alternative devices that are competitive and that have different characteristics, thus affording the engineer a choice of methods. The only lack in the materials of illumination is war-begotten and temporary. For practically all forms of electric lamps have gone to war. The more essential types are still being produced, but the number of types has been halved. Development of light sources is confined to military applications. New developments for civilian use wait upon victory. Any account of advances in light sources at this time must be lacking in news value. The principal American lamp manufacturers perform exceedingly well the task of supplying to engineers informative material concerning their several products. Their technical and commercial publications are comprehensive and informative, and they have responded helpfully to every request of the author for material for this article.

Arc Lamps

This oldest of electric illuminants has an interesting and honorable record of service in the earlier years of electric street lighting—a service that it has relinquished for the most part to a younger generation of illuminants. In other directions, however, it has become a lively and useful device. As a high-power projection source it is particularly valuable., In producing ultraviolet radiation it is also important. The principal American manufacturer of arc carbons indicates that, as is the case with most electric illuminants, the electric arc lamp is undergoing dynamic development, diverted for the present to war purposes, but promising for the postwar period. For instance:

> For searchlight work, experimental high-intensity carbons have doubled present-day performance, attaining to a brilliance of 2,000 candles per square millimeter, or better.
>
> Improvements in carbons, in applicable optics, and in methods of operation promise delivery of three times as much light to motion-picture screens as is attained commercially today.
>
> High-purity spectroscopic carbons are finding large use for war purposes in rapid checking of critical alloys for impurities. This method of analysis is used extensively in the aluminum, brass, and copper industries and is understood to be coming into extensive use in the steel industry.
>
> The carbon arc in latest development gives a close approach to quality of sunlight in the near ultraviolet region that is most effective in photochemical reactions.[2] These arcs are used largely in ac-

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Essential substance of an address presented under the sponsorship of the AIEE committee on production and application of light at the conference on lighting, AIEE winter technical meeting, New York, N. Y., January 26, 1944.

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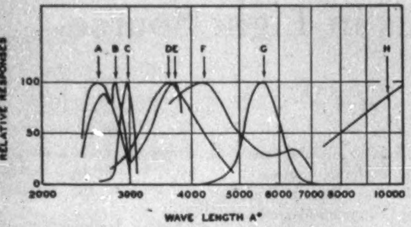


Figure 1. Some effects of radiant energy

A—Bactericidal
B—Vitamin D in sterols
C—Tanning
D—Photoprinting
E—Fluorescence—paints
F—Panchromatic film
G—Vision
H—Penetration of cheek

celerated weathering or fading tests in the protective coating, rubber, and textile industries. They are used also for the irradiation of milk, tobacco, and pharmaceutical products.

Tungsten-Filament Incandescent Lamps

In variety, range of sizes, simplicity, readiness of application, and wide acceptability, incandescent lamps are of very great importance in electrical service. American achievements in production and use of such lamps are pre-eminent.[3] The relatively low American rates for electric energy and the relatively low prices for American lamps have made possible notable advances in electric-lighting practice. Figure 3 shows the cost of producing a stated amount of light with tungsten-filament lamps for the year 1943 compared with the cost for the year 1921.[4] The entire rectangle represents the cost of light in 1921. The rectangle designated *A* represents corresponding 1943 cost. The reduction has been accomplished by advance in three elements of cost. Improved lamp efficiency (*B*) and lowered rates for energy (*C*) made the major contributions. Lowered lamp prices added considerably to the accomplishment. The combined result is that light which would have cost $10.21 in 1921, cost $2.86 in 1943.

TYPES AND SIZES

It is said that previous to the war there were some 9,000 distinctive types or sizes of lamps, including many for highly restricted applications. Approximately half of these types have been eliminated in the process of wartime conservation. Among the commonly used types so eliminated have been the 50- and 75-watt lamps and certain decorative types of lamps.

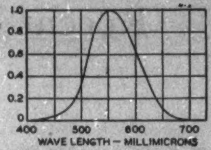
On the other hand, it is said that approximately 200 new types and sizes of incandescent lamps have been required for specialized war service. Lamp manufacturers have made a real contribution to the war effort through

1. The development of these new types of lamps as needed.*

2. Maintenance in high degree of the quality of their products despite deprivation of critical materials that to some of us might have seemed essential to the production of satisfactory-quality lamps.

In Figure 4 a cluster of charts shows for the record the approximate quantities of certain types of incandescent lamps sold during the last prewar year, chosen for this purpose as being more representative than the subsequent wartime years. Not included in these charts, but of importance, were 48,000,000 radio-panel lamps and 256,000,000 Christmas-tree lamps.

Figure 2. Relative luminosity (International Commission on Illumination standard)



The smallest lamp made is the so-called "grain-of-wheat" lamp, employed in surgical work. This consumes 0.17 watt and produces about one third of a lumen. The lamp itself is but 0.079 inch in diameter and 0.343 inch long. Manufacturers say that if sold by weight this lamp would cost about $7,500 a pound, because it weighs only 0.06 gram.

The largest incandescent lamp made is of 50,000 watts. It has a bulb 20 inches in diameter and weighs about 35 pounds. It produces 1,600,000 lumens or about the same amount of light as 1,000 100-watt lamps. The filament alone weighs about 1.6 pounds. One has the impression that this lamp was constructed somewhat as a stunt before the war when materials were not limited. It certainly has not entered much into practical use.

The range of application of the incandescent lamp is certainly remarkable. This will be appreciated when compared, for example, with the present range of other types of light sources.

RECENT DEVELOPMENTS

"Recent" in this connection means recently before the war. Among recent developments has been the extension of the all-glass sealed-beam lamps, devised originally for automotive head-lamp purposes in the 6- to 8-volt range, to make them available for 12- to 16-volt range for trucks

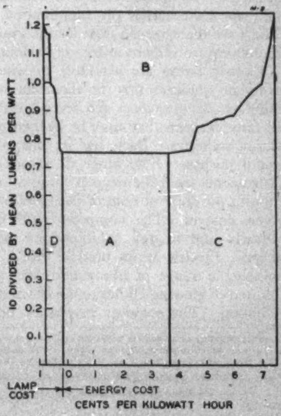


Figure 3. Lamp and energy cost of light from 60-watt specification-quality incandescent lamps (dollars per million lumen-hours)

A—Cost of light, 1943.........$ 2.86

Reductions since 1921

B—Through lamp efficiency increase.. 3.39
C—Through lowered rates for energy... 3.33
D—Through lowered lamp prices...... .63

A+B+C+D= cost in 1921.......$10.21

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* It is stated that a modern bomber plane uses about 200 electric lamps of various shapes and sizes. A battleship requires almost 30,000 lamps. A General Sherman tank (*M-4*) uses 27 lamps, and a full armored division requires about 60,000 lamps.

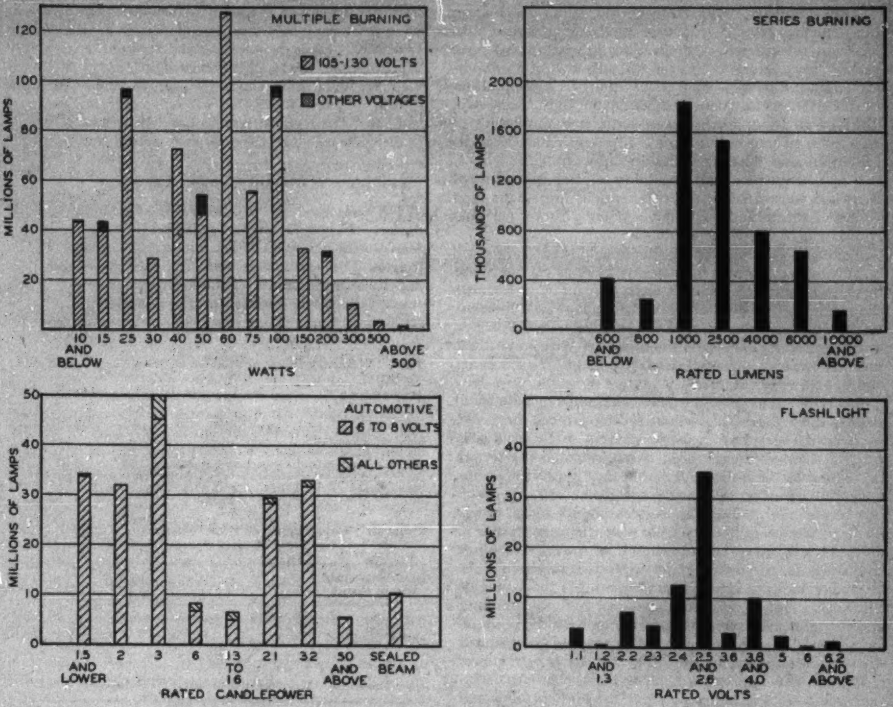

Figure 4. Quantities of certain types of tungsten-filament incandescent lamps produced in the United States in 1941

and 12- to 24-volt range for aviation work, in which sizes have gone up to as much as 600 watts. Smaller sealed-beam construction lamps were made available for aviation fields, spotlights, flashing signal lamps, and so forth.

Concentrated high-intensity illumination for indoor use and with hard pressed-glass bulb for outdoor use is available in reflector-embodied lamps.

EFFICIENCY

Incandescent lamps are rated in terms of lumens per total watt input. An analysis of the energy input to a 60-watt tungsten-filament lamp of modern construction is shown in Table I.[5]

There has been a steady advance in the efficiency of light output of incandescent lamps in recent years. The 7.8-lumen-per-watt 60-watt lamp first introduced had become a 13.9-lumen-per-watt lamp. The range of efficiencies of modern tungsten-filament lamps is from 4.5 lumens per watt in the case of three-watt 120-volt lamps to about 36 lumens per watt in the case of the short-life photoflood lamps. Among lamps generally employed for illumination purposes the range is approximately 9 to 21 lumens per watt. It is probably a fair estimate that incandescent electric lighting is accomplished with a light-production efficiency of about 13 lumens per watt. This is comparable to about 40 lumens per watt for fluorescent lamps. Maximum efficiency of solar radiation has been estimated at approximately 86 lumens per watt, corresponding to black-body radiation at 6,500 degrees Kelvin.[6]

PERFORMANCE

The characteristic performance of specification-quality lamps* of the usual residence-lighting types is shown in Figure 5. The 60-watt lamp, the most widely used size, declines about ten per cent in lumens to the end of its 1,000-hour life and about seven per cent in efficiency. Rated lives of other lamps range from 2 hours for photoflood lamps to 3,000 hours in the case of some types in which replacement is difficult. The life of incandescent lamps, of course, depends upon the filament temperature, varying inversely as about the seventh power of the lumens per watt. For lamps entering into general lighting service, conventional rated lives are 1,000 and 750 hours for residence types, 750 hours for larger commercial lamps, 1,500 hours for sign lamps, and 3,000 hours for group-replacement street series lamps.

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* Specification-quality lamps are products which have been made to comply with the requirements of the Federal Specifications for Incandescent Lamps.

The simplicity, interchangeability, and convenience of application that characterize incandescent lamps have led to a great variety of applications in which it has been practicable to embody most of the advances of illuminating-engineering knowledge with regard to such qualities as direction of dominant components of light, degrees of diffusion of light, intensities of light, and limitations of brightness of exposed sources. The result has been excellent advance in electric-lighting practice.

The convenience and flexibility of the incandescent lamp are illustrated in the two-filament three-light type which has attained such large popularity in portable equipment in residence lighting.

Among disadvantages of the incandescent lamp are sensitiveness to voltage fluctuations (a 5 per cent decrease in volts causes a 15 per cent reduction in light output); the production of an undesirable amount of heat incident to the lighting process; and the relatively low efficiency of light production—low in comparison with some light sources, but high in comparison with lamps of a few years ago.

Favorable qualities of incandescent lamps include practically instantaneous starting (accompanied, however, by a large short-time inrush of current to the cold filament); filament thermal lag that reduces cyclic flicker in a-c service to an extent that makes it practically inappreciable under ordinary conditions above 40 cycles per second; and close approximation to unity power factor. The ready facility with which filament lamps of different sizes may be substituted for each other must count also as an advantage for incandescent lamps. An attractive feature of incandescent lighting is the agreeable color of the light. Humans have evolved under sunlight and firelight. The daylight period has been the occasion for all kinds of activities; the firelight period is associated with comfort, safety, repose. The two form extremes of radiation that approximate black-body character. Incandescent electric lamps confront the human mind and the human eye with a light color quality that is agreeable.

INFRARED LAMPS

The radiation from 7,800 to 20,000 angstroms is sometimes called the "near infrared." The region beyond 20,000 is called the "far infrared." Glass is practically opaque to radiation of longer wave lengths than 40,000. Glass filters having high transmission in the infrared and high absorption in the visible have been developed. Filaments in drying lamps are operated usually at about 2,500 degrees Kelvin.[7] Such filament lamps have been used successfully for radiant-energy drying in baking, heating, and evaporating moisture, and for softening, expanding, or treating materials. Sealed drying lamps of 500- and 1,000-watt sizes are equipped with cover glass of the kind mentioned.

Tungsten incandescent lamps have peaks in the infrared as shown in Table II. Wave lengths of the order of 10,000 to 12,000 angstroms produce maximum penetration of aqueous bodies. Lamps provide peak penetration at about 11,000 angstroms.

The use of radiant energy from drying lamps[8] offers opportunity for conserving space and for effecting economies with increased production in numerous industrial drying operations. In manufacturers' catalogues drying lamps are listed in variety that appears to be quite adequate to all demands. Technical information regarding their use is supplied in readily usable form. In the application of these lamps there is opportunity for engineering ingenuity. Since the materials and the engineering ability are available, this application of light sources rapidly is assuming great importance.[9,10] Manufacturers' literature on the subject features, not without reason, such expressions as "The modern way to bake, dehydrate, or dry"; "faster, cheaper, and easier drying, baking, and heating."

Mercury-Vapor Lamps

The mercury-vapor arc lamp is a remarkably versatile device. Its characteristic radiation lines vary in intensity with temperature and pressure throughout a wide range. The ultraviolet spectrum lies between 4,000 and 1,000 angstroms.[11] Ultraviolet energy from the sun reaches the earth's surface down to wave lengths 2,900 to 2,800 angstroms. The mercury-vapor lamp can be made

Table I. Energy Input to Modern 60-Watt Tungsten-Filament Lamp

	Per Cent
Rated visible spectrum	7.5
Gas loss—heat convection and conduction	13.5
End loss—conduction by leads and support wires	1.2
Filament radiation (an additional 6 per cent bulb and base heat absorption is involved)	80.8

Table II. Wave Length of Peak Radiation of Tungsten Incandescent Lamps

Color Temperature (Degrees Kelvin)	Angstroms
2,150	12,000
2,800	10,000
3,300	8,000

Table III. Mercury-Vapor Lamps Produced in 1942

Watts Per Lamp	Thousands of Lamps Per Year
Bactericidal Lamps	
4	3
5	10
15	24
30	2
Sun Lamps	
100	11
130	5
275	12
400	18
Various	5
For Illumination Purposes	
Type *H*	
100	15
250	21
400	127
1,000	3
Cooper-Hewitt	
All sizes	53

This table does not include high-power therapeutic radiators for use under medical direction and in certain industrial processes, of which it is understood that more than 5,000 were sold in 1942.

Table IV. Light-Producing Efficiencies of Principal Type-H Lamps

Lamp Watts	Rated Initial Lumens Per Watt	Lumens Per Watt at 70 Per Cent Rated Life
100	35	30
250	30	25
400	40	35
3,000	40	

Table V. Spectral Characteristics of Some Mercury Lamps

Watts	Relative Ultraviolet (Below 3,800 Angstroms)	4,358 Angstroms (Blue)		5,461 Angstroms (Green)		5,780 Angstroms (Yellow)		Continuous (Visible)		Relative Infrared (7,600–26,000 Angstroms)
		Lumens	Per Cent	Lumens	Per Cent	Lumens	Per Cent	Lumens	Per Cent	
H-1..400	2.1	110	0.7	7,860	49.1	6,800	42.5	1,230	7.7	40.9
H-2..250	1.2	55	0.7	3,770	50.3	3,245	43.3	430	5.7	14.6
H-4..100	1.6	25	0.7	1,735	49.6	1,255	35.9	485	13.8	14.7
H-5..250	4.6	70	0.7	4,630	46.3	4,040	40.4	1,260	12.6	32

to radiate within this region, and also in considerably shorter wave lengths and in longer wave lengths.

Radiation in the region 4,000 to 3,200 angstroms is most useful for photochemical effects. That between 3,000 and 2,400 angstroms is valuable for activation of vitamin *D* in sterols. The region below 2,800 angstroms is most effective in destroying microorganisms. All of these purposes are served by mercury-vapor lamps. The low-pressure lamps, with strong radiation in the 2,537-angstrom line are of value for bactericidal purposes. The sun lamps, with strong radiation at 2,967 angstroms, are effective for erythemal, tanning, and antirachitic purposes. Both low- and high-pressure lamps have usefulness in the photochemical and industrial-illumination fields. The very high-pressure water-cooled lamp is a concentrated source of light, valuable for projection purposes. Certain smaller mercury-vapor projection lamps have a variety of applications, including bactericide and theater lighting.

A mercury arc in a glass tube produces illumination. Line that tube with phosphors, and it becomes a fluorescent lamp. Replace the tube with fused quartz or ultraviolet transmitting glass, and it becomes an ultraviolet radiator. Yet it is the same kind of mercury arc!

Quantities Produced. The approximate quantities of certain types of mercury-vapor lamps produced during 1942, as reported by principal manufacturers are shown in Table III.

FOR ILLUMINATION PURPOSES

Light-producing efficiencies of the principal type-*H* lamps are listed in Table IV. These lamps require some few minutes for starting and, if the circuit is interrupted, will not restart immediately. They are, of course, suitable primarily for use in industrial installations. They are often used in connection with incandescent lamps of high wattage to produce better color value and to insure against full outage. Abroad, especially in England, these lamps have been used chiefly for street lighting. In cities they were thought to complement show-window lighting nicely, while in suburbs they illuminated the green foliage pleasingly.

Color. The color of mercury arcs is so largely subjective that attempts to compare it with daylight are complicated. Buttolph[12] makes graphic comparison of an incandescent lamp, a sodium lamp, the Cooper-Hewitt lamp, and a high-intensity mercury lamp, by assigning to the spectral lines widths such that their areas are proportional to their relative intensities, and the total line areas represent the visible energy necessary to produce equal luminosities. Trichromatic sensations are hardly applicable in the case of these line-spectra sources. However, in the high-pressure mercury lamps there is shift away from shorter- to longer-wave-length energy, and a continuous background appears in the spectrum, with betterment of color quality for most illuminating purposes. Some spectral qualities of certain high-pressure mercury lamps are cited in Table V.[11]

ACTIVATION OF FLUORESCENT MATERIALS

The high-pressure mercury-vapor lamps that are catalogued as *H*-3, *H*-4, and *H*-5 are of high efficiency and, producing 3,660 angstroms radiation, are useful for activation of fluorescent materials used in theatrical and decorative work. They are useful too in many photoprinting applications that are dependent upon high intensity of near ultraviolet. The *H*-3 lamp is being used in printing the sound track on films. The *H*-4 lamp in a reflector red–purple bulb is much used for exciting fluorescence.

A phosphor designated 360 *B-L* is utilized in mercury-vapor lamps ranging from 3 to 40 watts. It converts the 2,537 mercury radiation to energy radiated between 3,000 and 4,200 angstroms with relatively little radiation in the visible spectrum. The 2,537 radiation is substantially suppressed. This lamp is useful for activating fluorescent materials and in certain photochemical processes. A four-watt d-c lamp of the type just mentioned is used for illuminating airplane instrument panels.

HIGH-PRESSURE QUARTZ MERCURY-ARC LAMPS

Ultraviolet radiation from these sources is relatively greater than in the low-pressure lamps, though the line of greatest radiation may not be so well adapted for some purposes while being better adapted for others. These lamps, operating at from 10 to 100 atmospheres with quartz inner bulb, have spectra that approach the continuous with a line spectrum superimposed. The 1,000-watt water-cooled lamp produces about 65,000 lumens initially at 65 lumens per watt or, if the auxiliary be taken into account, at about 54 lumens per watt. It is employed in the laboratory, having a life of only about 50 hours.

High-pressure quartz mercury arcs are used in various photochemical works. They are a preferred source for irradiating milk and other foodstuffs. They are employed in the synthesis of rubber and the curing of enamel coatings and patent leather, and find wide application in tests of fastness of dyes, purity of paints, and aging of fabrics. Such sources are used also in blueprint machines, in photo-

Table VI. Results of Experiments With Hot-Cathode Germicidal Lamps

In a 4,000-Cubic-Foot Room

Watts Per Lamp	Equivalent to Air Changes Per Hour
30	100
15	45
8	40
4	10

Table VII. Typical Phosphors Used in Fluorescent Lamps

Phospher	General Color	Exciting Range* (Angstroms)	Sensitivity Peak (Angstroms)	Emitted Range (Angstroms)	Emitted Peak (Angstroms)
Calcium tungstate	Blue	2,200–3,000	2,720	3,800–7,000	4,400
Magnesium tungstate	Blue–white	2,200–3,200	2,850	3,800–7,200	4,800
Zinc silicate	Green	2,200–2,960	2,537	4,500–6,200	5,250
Zinc beryllium silicate	Yellow–white	2,200–3,000	2,537	4,500–7,200	5,950
Cadmium silicate	Yellow–pink	2,200–3,200	2,400	4,300–7,200	5,950
Cadmium borate	Pink	2,200–3,600	2,500	4,000–7,200	6,150
360 *BL* phosphor‡	Blue ultra	2,200–3,200		3,200–4,000	3,600

* 2,200 angstroms is lower limit of measurements.

‡ Used for exciting fluorescent chemicals, blueprinting, and so forth.

printing apparatus, in the activation of vitamin *D* in foodstuffs and in pharmaceutical preparations.

Sunlamp. Luckiesh[13] in "Artificial Sunlight" states that in summer on a clear day at noon erythemal energy is received at the earth's surface at about equal parts from the sun and from the total sky. At other times the ultraviolet from the sky exceeds that from the sun. Sunlight spectra extend down to about 2,900 angstroms. Porter[14] describes sun lamps in terms of erythemal power with spectral curves. The most powerful wave length in producing erythema is at 2,967 angstroms. The erythemal effectiveness curve from 2,800 to 3,180 angstroms is shown.

There are five sun lamps having different radiation characteristics with the *S*2 lamp producing about one third of the ultraviolet that is produced by the others. Irradiation of the body is one way to produce vitamin *D*. Coblentz[15] notes that radiation of 2,540 angstroms does not penetrate deeply into the skin. Erythemal effects are superficial. On the other hand, radiation of 3,100 and longer beyond erythemal dosage does penetrate and may produce a blister. In addition to application to the human body, experiments in irradiating farm animals and poultry have produced successful results, as, for instance, in increased production and hatchability of eggs.[16] These lamps offer an alternative method of prevention and cure of rickets.

BACTERICIDAL LAMPS

Within the ultraviolet range the region 3,000 to 2,000 angstroms is considered to have germicidal value.* Bactericidal action is said to be at a maximum at approximately 2,660 angstroms, decreasing for shorter and longer wave lengths, but rising again for radiation below 2,375 angstroms. This is for the generality of pathogenic bacteria concerning which some information is available. There are known exceptions.

Within this bactericidal range the 2,537-angstrom resonance line of mercury is excellently placed for effectiveness. About half the radiation from low-pressure mercury-arc lamps is in this 2,537-angstrom band. These lamps, equipped with ultraviolet transmitting glass, therefore are an efficient and economical source of bactericidal radiations. They are available in two types as follows:

Cold cathode, known to the trade as "Sterilamp," is rated at 15 watts. It starts at about 575 volts and operates at about two-thirds that value.

Hot cathode, known to the trade as "germicidal lamps," are available in 30-, 15-, 8-, and 4-watt sizes. Except for the absence of phosphors and the use of ultraviolet transmitting glass, they are similar to fluorescent lamps. These lamps decline in efficiency with low temperatures. At freezing, the output is 60 per cent; at 0 degrees Fahrenheit it is 30 per cent of normal.

Certain infections, including common colds, are believed to be carried by air-borne bacteria or virus. Energy radiated in the stated regions offers a practicable means of killing such bacteria. Installed in ventilating ducts, where that is feasible, or so placed as to irradiate the upper stratum of air in a room where there is fair circulation, these ultraviolet radiators offer means of purification of the air that is analogous to the purification of drinking water.

Experiments as to the lethal effectiveness of such ultraviolet radiation have been made principally with bacilli coli, a rather harmless organism having characteristics thought to be similar to those of many pathogenic bacteria. Working with these bacilli coli, experimenters have reported achievement by these means of an equivalent of air replacement of 4,000 cubic feet per minute per person, which is 10 to 15 times the actual air replacement needed to keep carbon dioxide down to a satisfactory level. It has been reported further that one of these ultraviolet radiators in an average-sized duct will kill over 90 per cent of the bacteria in the air passing through the duct at a rate of 500 to 600 cubic feet per minute.[17]

A single such radiator irradiating the upper part of an average-sized office is said to be capable of reducing the bacteria count as much as could be accomplished by opening all of the windows on a breezy day. Other reports are that one small ultraviolet lamp irradiating about 3,000 cubic feet of air has a sanitary effect equivalent to 50 or 100 air changes per hour.[18]

Irradiation of the upper stratum of the air in a room for bactericidal purposes is sometimes referred to as "sanitary ventilation."* The effectiveness of such methods is lower when humidity is higher.

In certain experiments with hot-cathode germicidal lamps the results shown in Table VI have been observed.[19] There seems to be no doubt that under properly controlled conditions such ultraviolet radiation is effective in killing air-borne microorganisms and may be used to supplement other measures for the prevention of cross infection. Experiments thus far are promising and indicate the possibility of a large and useful application in the near future. In places where there is illness or where there are large assemblages of people the applicability seems immediately indicated. Hospitals, schools, theaters, passenger carriers, and the like might benefit. Since it has been estimated that an average of 400,000,000 colds a year cost the nation half a billion dollars, the prospect of being able to reduce infection through air purification is very attractive.

It is interesting to contemplate the value that these bactericidal lamps may possess for occupants of heated submarines and heated cabins of stratosphere airplanes. Regions inhabited by mermaids and gremlins may be provided with California climate!

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* Mayer[11] notes that germicidal effects differ with different bacteria. 3,100 angstroms is a lethal radiation wave length for some but action in other cases has been observed at 2,250 angstroms.

* A popular article describing successful results of experiments in preventing cross infections in cases of measles and colds appeared in the *New Republic* for December 15, 1941, under the authorship of Bruce Bliven. It was condensed in the *Readers' Digest* for January 1942.

Sterilization. Ultraviolet radiators are entering into use in hospital operating rooms for prevention of cross infection and of infection in wounds. It is said that a six-to-one reduction in the incidence of wound infections in certain types of operations has been shown in records of two hospitals. Such lamps for use in operating rooms, nurseries, and hospital wards, where they are used under medical supervision, are listed by the American Medical Association as "Accepted." However, sources radiating appreciable energy of wave lengths shorter than 2,800 angstroms are not recommended for use by laymen for therapeutic purposes.

Smallest of the ultraviolet lamps is an arc in a quartz tube within a container that is about $10^{1}/_{2}$ by 3 inches. These are provided for treatment of slow-healing wounds, abscesses, ulcers, and so forth.

Miscellaneous Applications. Sanitizing uses to which ultraviolet radiators are being applied include:

Meat storage places
Restaurants
Bakeries
Sterilizing special canners' sugar
Hospital bathrooms
Industrial washrooms
Water sterilization

Conservative Application. In the realm of prophylactic and therapeutic practice, as well as in the field of health generally, a great deal of conservatism is required from engineers. A suggestion as to health benefits attributable to ultraviolet radiation is likely to give rise to ill-founded and extravagant commercial claims by irresponsible people. Much of the experimentation in this field has been carried on by commercial interests that are desirous of selling equipment for this purpose. Engineers know leaders in this experimental field and respect them. Others, however, may draw from their work far-reaching conclusions that are hardly justified. It would appear that there is a need for more independent disinterested experimentation in ultraviolet applications for prophylactic and therapeutic purposes. Indications point to an important and growing field of usefulness for these sources of radiation. It is important that it be developed conservatively. Ultraviolet radiators are no panacea for either prevention or cure. They are useful devices that may be expected to fill an increasingly important place in the future.

Sodium-Vapor Lamps

Unlike other lamps, the sodium lamp[20,21] is not at all versatile. No matter how it is treated, it sturdily insists upon radiating its light at 5,893 angstroms. Before the war these lamps were being sold at a rate of about 11,000 a year. They are used for highway lighting and lighting of traffic intersections where a different effect is sought in order to keep motorists alert. They have also been used for lighting railway yards and airfields. The monochromatic light should have value wherever high visual acuity is involved and where the color characteristic is unobjectionable. Since the radiation is at a wave length close to that of maximum eye response, the sodium lamp is a highly efficient illuminant, the 10,000-lumen lamp producing light at about 50 lumens per watt with auxiliary loss included.

Fluorescent Lamps

The genealogy of the fluorescent lamp traces back to the Cooper-Hewitt mercury-arc lamp[22] and the Claude neon tube.[23] Electrically it is an arc discharge through a low-pressure atmosphere of mercury vapor and rare gases. The literature of the art records the distinguished development of the mercury-arc lamp into regions of higher and higher temperatures and pressures with expanding fields of usefulness. It records also the advances in sign lighting, including the employment of gases or vapors other than neon, to produce the ingenious colorful outdoor displays with which we in America are most familiar. In the last few years these two illuminants, while continuing to serve with increasing effectiveness in their distinctive fields, have been developed through the addition of fluorescent materials, called phosphors, to afford valuable service in the field of general illumination.

As a result there are now mercury-vapor lamps from which but little of the light produced in the characteristic mercury-radiation lines of the visual spectrum emerges, but from which a relatively large amount of light is radiated from the fluorescent materials that are activated by the ultraviolet energy of the mercury arc.

Ballasts. To stabilize the arc some form of ballast is required. Probably the simplest form of ballast is the series resistance, as now used in d-c service, but, because of its energy consumption and consequent reduction in operating efficiency, this arrangement finds little use. The most commonly used ballast is an inductive reactance, obtained either directly in the form of an iron-core choke or indirectly through a high-leakage transformer. Both of these arrangements unless modified give operating circuits of low power factor.

Starting. Two methods currently are employed for striking the arc:

(*a*). To provide a special initial ionization of the arc stream.

(*b*). To apply an initial voltage higher than operating voltage.

The first was developed in mercury-vapor-lamp practice; the second in operation of gaseous-discharge electric-sign tubes.

Superior Efficiency. The greatest advantage of fluorescent lamps over the generally used incandescent lamps is the production of two or three times as much light for the same expenditure of watts with less accompanying heat. This makes practicable the attainment of the higher levels of illumination that production efficiency demands where efforts are dependent upon visual ease. And this important objective can be attained without overburdening existing wiring systems or with lesser wiring costs in new systems. Furthermore it makes available an abundance of light without undue elevation of ambient temperatures and without handicapping air-conditioning systems.

Output Maintenance. All fluorescent lamps containing mercury depreciate in light output as they operate. The rate of depreciation varies for tubes of a given diameter with the current density and other factors.

COLOR OF LIGHT

Synthetic fluorescent materials have been developed to produce light of a great variety of colors. These are mixed as desired. The generally reported phosphors as commonly used in fluorescent lamps are listed in Table VII.[24]

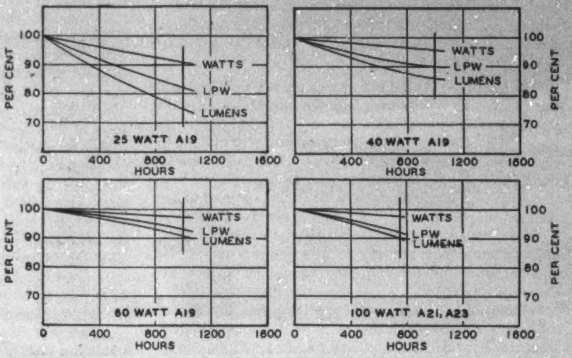

Figure 5. Performance characteristics of specification-quality tungsten-filament incandescent lamps

The most largely used fluorescent lamps are of the types designated "white" and "daylight," respectively. The white lamp produces light of a color that has found wide acceptance in industrial and commercial installations. It blends well with natural light. Because of the one-tenth elemental mercury radiation, it has a barely perceptible yellow–green cast that renders it unsuitable for some color-critical purposes. The light of the daylight lamp, second in popularity, has a similar characteristic but is stronger in the blue and less acceptable for use in places of assemblage.

A fundamental difference between light of fluorescent lamps and either daylight or light of incandescent lamps is the presence in the former of the spectral lines of the normal mercury discharge and a deficiency in the deep red.[25]

The color difference is somewhat difficult to depict. In Figure 6 a comparison is made between spectrophotometric curves of

3,500-degree-Kelvin black-body radiator[26]
300-watt tungsten-filament lamp (2,905 degrees Kelvin)[27]
3,500-degree-Kelvin fluorescent lamp[28]
Noon sunlight[29]

The mercury-line radiation is superimposed upon the continuous spectrum of the light from the phosphors. The vertical bars around lines representing, respectively, wave lengths 4,047, 4,358, 5,461 and 5,780 angstroms are adjusted so that the enclosed area upon the spectral curve indicates the magnitude of the energy radiated in these respective lines. Light of day and that from incandescent sources lie close to the locus of black-body radiation. The light from fluorescent sources has distinctive characteristics. The effect of the yellow–green mercury lines is to exalt the brilliance of corresponding yellow and green pigments to a degree that is not experienced in other light of equivalent synthetic ocular value. Red pigments generally are dull under fluorescent light.

A so-called "soft-white" lamp, designed to provide more acceptable appearance of meats, foods, complexions, and so forth, was made prior to the war but has been eliminated as a part of war curtailment. Its light contained more of the orange and light red elements and emphasized less the yellow–green colors. At a loss of about 25 per cent in efficiency there was achieved a color of light that for many purposes was pleasing. When its manufacture will become practicable again, something of the kind may be expected to be placed on the market.

Such information as is available indicates that there is no material change in the color of light from fluorescent lamps throughout their operating life.

Cyclic Flicker. In lamps that operate on 60-cycle current, the arc is extinguished 120 times a second. The visible radiation from the phosphor that produces blue light varies almost instantly with the change in activation. The light from other phosphors lags a bit but follows the variations in activating energy very largely. The result, on alternating current, is a pronounced cyclic flicker of light modified by a slight color flicker. On 60-cycle circuits the relative cyclic flicker expressed in per cent deviation from mean light output has been found to be

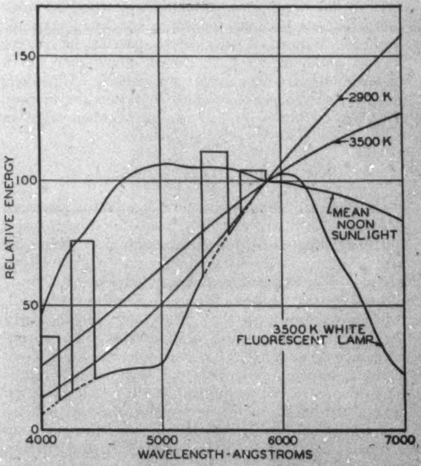

Figure 6. Comparative spectral distributions

Table VIII. Relative Cyclic Flicker on 60-Cycle Circuits

Per Cent Deviation From Mean Light Output

Light Source	Relative Cyclic Fluctuation
100-watt tungsten-filament lamp	5
Red fluorescent lamp	10
40-watt tungsten-filament lamp	13
2 white fluorescent lamps on phase-offsetting auxiliary	16
Green or pink fluorescent lamp	20
Gold fluorescent lamp	25
2 daylight fluorescent lamps on phase-offsetting auxiliary	25
Single white fluorescent lamp	35
Single daylight fluorescent lamp	55
Single blue fluorescent lamp	90

approximately as shown in Table VIII.[31] Uncritical observers may ignore such light flicker, especially in installations in which the need for refinement in lighting results is not very urgent. In critical service, however, it requires to be modified to lessen its stroboscopic effects.

The cyclic light flicker may be reduced to an unobjectionable level where several lamps are used to illuminate the same area if, polyphase service being available, the several lamps are connected to different phases, thus offsetting the maximum–minimum extremes of light production.* This arrangement, however, leaves the power factor unsatisfactorily below unity and in need of correction.

Brightness. The brightness of white and daylight fluorescent lamps is close to the acceptable limit of exposure in the field of view. It is so close indeed that in the industrial installations where ceilings are of good light reflectance and the fluorescent lamps are mounted high and cover a large part of the ceiling area, some think it acceptable to operate the lamps unshielded. It is the consensus of opinion, however, that in other installations the lamps should be shielded to reduce their brightness to acceptable values. This becomes especially important where exacting visual tasks are performed and where the lamps may enter into the ordinary field of view.

Vision Under Fluorescent Lighting. Through experiments and reasonable deductions, it has been concluded that there is no appreciable difference in vision attributable to the differences in quality of light from, respectively, "daylight" fluorescent lamps and tungsten-filament lamps.[30]

Design. Apparently in several important aspects of fluorescent lamp design, there is an optimum above and below which less favorable results are obtained. This seems to apply to tube dimensions, arc current, activating energy, combination of phosphors, and so forth.

In Critical Installations. Humming noises from auxiliaries and radio interference are aspects of fluorescent-lighting equipment that must be guarded against in critical installations, as in home-lighting service.

Types of Fluorescent Lamps. There are available currently three forms of fluorescent lamps evolved through the lines of descent that have been described. They are, respectively, the multiple low-voltage hot-cathode lamps; the higher-voltage cold-cathode lamps, and the rectified low-voltage hot-cathode lamps. Statements in the foregoing discussion of "Fluorescent Lamps" are generally applicable. On the following pages are comments that apply only to one or another of the types.

MULTIPLE LOW-VOLTAGE HOT-CATHODE FLUORESCENT LAMPS

Some of the electrical features of these lamps are considered in the discussion of mercury-vapor lamps. The range of these fluorescent lamps for general illumination purposes at present is from 15 to 100 watts. The 15- to 40-watt sizes, inclusive, fit standard lamp holders. The larger-size lamps fit into Mogul lamp holders. These lamps, however, are not generally interchangeable as to size. Because of different lengths, spacings between lamp holders are different. There also are four-, six-, and eight-watt lamps that fit into miniature lamp holders.

Transformers. The 15- and 20-watt lamps that do not require higher than line volts for starting are operated without transformers. The 30- and 40-watt lamps, requiring a minimum of 200 volts open circuit for dependable starting, are provided with transformers for operation on 120-volt circuits. They may be operated without transformers on the 208-volt supply lines that commonly are available in both industrial and commercial installations. The 100-watt lamp is sometimes operated without transformers, the lamps being connected two in series and operated with proper ballasts from a 265-volt source that is available in many industrial installations. (Phase voltage corresponds to a 460-volt four-wire three-phase supply.) Special four-lamp ballasts are available for this application.

Ballasts. Growing out of practice in the operation of high-intensity mercury lamps[31] has come the operation of a pair of lamps on a ballast in which a capacitor is used to provide a lead current for one lamp that is out of phase with the lagging current of the other lamp. A compensator is placed in this lead lamp circuit to insure adequate preheating of the electrodes. Such operation insures both satisfactory power factor and adequate reduction of the magnitude of light flicker. From the better power factor come reduced wiring costs and advantage for generating and distributing equipment. From the reduced light flicker come diminished stroboscopic effects that otherwise would be objectionable in critical installations. This type of corrected auxiliary is very generally used.

The four-lamp ballast for 100-watt lamps previously mentioned accomplishes similar power-factor and light-flicker corrections. It achieves a small gain in lumens per watt of the lamp and its equipment.

Flicker Correction. In discussion of fluorescent lamps in general, the magnitude of cyclic fluctuations of the light of fluorescent lamps was compared with that of incandescent lamps. The advantage in light-flicker reduction accomplished through the use of twin lamp ballasts is shown in Table VIII.

Starters. A starter in the circuit of each lamp delays for a few seconds the application of voltage while the electrodes are being preheated. This delayed start is not necessary but is provided in the interest of good performance of the lamp. Failure of the lamp usually occurs through decline of the emissivity of the electrode. The rate of this decline is greatly affected by the number and frequency of starts. In the laboratory with one start every three hours, a life of well over 3,000 hours is obtained. A starterless ballast is practicable but in the present stage of development is not advisable because of impairment of lamp life.

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* For other means of obviating objectionable flicker, see discussion under multiple low-voltage hot-cathode lamps.

Because the starting current is about double the operating current, the ballast of fluorescent lamps will become overheated if a deactivated lamp is permitted to remain in the circuit. To obviate possible difficulties, a type of starter recently put into production includes a thermal switch in series with the starter timing switch. When the fluorescent lamp fails and the starter makes a few attempts to establish the arc, the resultant heat opens the thermal switch which remains open until the failed lamp is replaced. This eliminates the flow of current through the lamp auxiliary, reducing the watts dissipated at the transformer. An extinguished lamp seems a more acceptable signal for replacement than does a blinking lamp. And the likelihood of damage through elevated temperature in the ballast is eliminated. These "no-blink" starters are now available for 40- and 100-watt lamps. They are available in both the automatic and the manual reset type.

Efficiency. The optimum operating temperature for hot-cathode fluorescent lamps is an ambient of the 100 to 120 degrees Fahrenheit range. Above and below this range the output and the efficiency fall off. Only 25 to 35 per cent of the total energy dissipated is radiated from fluorescent lamps in the infrared region. Amick[24] analyzes the 40-watt input as given in Table IX.

It is evident that in comparison with the gas-filled tungsten lamps, in which 60 to 75 per cent of the energy is radiated in the infrared region, the light from fluorescent lamps is relatively cool light. According to this analysis, about 18.5 per cent of the applied watts is converted in these fluorescent lamps into light within the visible spectrum.

The rating of these lamps as to light-output efficiency is customarily based upon the watts delivered to the lamp, exclusive of that absorbed in the auxiliaries, which for two lamps on a "tulamp" ballast is about 17 watts. This form of rating is used presumably because the watts dissipated in the auxiliaries depend upon the type of auxiliary used. On this basis the 40-watt white lamp, after the decline of its initial 100-hour period, produces approximately 52 lumens per watt. The manufacturers seem to feel that a ten per cent increase is probable in this efficiency of light production. The depreciation in efficiency at 70 per cent of rated life is about 15 per cent. This the manufacturers believe that they are on the way to improving quite appreciably, perhaps cutting the rate of decline almost in half.

These fluorescent lamps are quite sensitive to changes in temperature of ambient. For specimen lamps of several sizes ranging from 15 to 100 watts as produced in 1941, the variations under reasonable test conditions are as shown in Figure 8. Within the usual range of indoor temperatures, say 50 to 90 degrees Fahrenheit, variations in efficiency are not more than five per cent.

The sensitiveness of these lamps in this respect is a nightmare to photometrists who seek reproducibility of test results. With temperature and other conditions well-controlled in a laboratory, variability due to changes in the ambient temperature is minimized in the case of bare lamps. When, however, these lamps are used in any kind of housing or in a reflector, the temperature of the ambient varies with the type and position of the enclosure. In such case it is important to be sure that the lamp is operated in the same position as that in which it will be used and to make photometric measurements without alteration of that position.

Use of the hot-cathode fluorescent lamps outdoors in cold weather is inadvisable unless special precautions are taken to avoid starting troubles.

Applications. The use of fluorescent lighting was growing by leaps and bounds until the war put an end to its development except for Government and war-production purposes. In the last two years new installations have been limited chiefly to such uses. Most recent experience therefore has been in industrial and Government office-building lighting. When materials and manufacturing facilities are released from war service, it is probable that the rapid increase of prewar months will be resumed and accelerated in industrial and commercial lighting fields.

Many people also are looking for extensive development of fluorescent lighting in residences. In the utility rooms of the home, as the kitchen and laundry, fluorescent lamps may be expected to find ready application. It seems apparent that in new or reconstructed buildings long slender sources can be embodied architecturally to secure effects that may be wanted in some cases. High voltages, however, are to be avoided. When portable lamps and attached lighting fixtures are considered, the large physical dimensions of the fluorescent lamps required to secure adequate light flux seem likely to intrude as a difficulty. Small sources that lend themselves to dainty and graceful structures for small rooms are desirable. Circular fluorescent lamps that may become available will approach this objective, but it will be difficult to get enough light from them to provide general illumination.

In case these difficulties are overcome, there still will remain the question as to the desirability in most cases of trying to substitute fluorescent for incandescent lamps in the home. If the fluorescent-lighting equipment is to be well done and to serve for satisfactory lighting, it seems likely to be more costly than incandescent lighting. It will be more complicated, and the acceptability of the color of fluorescent light remains to be established. Doubtless when the war is over, light of color that will be more pleasing for residence use than is the present "white" lamp will be made available. Perhaps the former "soft white" will be restored. So long, however, as the radiation in the characteristic mercury lines produces something like ten per cent of the total illumination, the acceptability of fluorescent light for other than utilitarian rooms of the home may be questioned.

Prospect as to Types of Fluorescent Lamps. The trend has been toward bigger and better fluorescent lamps, but the optimum size may already have been reached. As with illuminants in general, economical lighting is achieved through standardization upon a few types that can be produced in large quantities. As always, there is a demand

Table IX. Analysis of 40-Watt Input of Hot-Cathode Fluorescent Lamps

	Conversion of Energy Within the Lamp (Watts)	Ultimate Conversio (Watts)
Light	0.9	7.4
Ultraviolet radiation (55 per cent at 2,537 angstroms)	22	
Heat	17.1	Radiated ......10.9 Convected and conducted....21.7

for specialty lamps to meet special requirements. As always too, something that is different can be sold merely on the ground of difference. Conspicuous among the desires that have been expressed is that for circular lamps, already mentioned. The manufacturers report that they can make such lamps when the war is over, but they apprehend that the cost will be relatively high, and the demand for them may be less than some people expect.

It might appear that the production of circular fluorescent lamps is likely to be attended with considerable difficulty. There will be questions as to the kind of ballasts and starters to be used, and as to provision for minimization of radio interference and cyclic flicker, as well as for good power factor. The type of lamp holders to be used and their location may offer some difficulty. It would seem also that the manufacturing process might be more complicated in some particulars than for straight-tube lamps. On the whole, higher cost and possibly less satisfactory performance are to be expected from circular lamps.

Cooper-Hewitt Rectified Fluorescent Lamps. In the original Cooper-Hewitt mercury-vapor lamp[32] a-c operation was accomplished by resorting to a rectifying circuit that provided a pulsating d-c discharge through the lamp. Recently this type of lamp has been equipped with phosphors for production of fluorescent light for industrial installations. There are 85-watt lamps rated at 4,000 lumens, 47 lumens per watt for the lamp alone. These lamps are available in two colors, the one called blue–white and the other called industrial white. The blue–white lamp "emphasizes the cooler end of the spectrum." The industrial white lamp gives a somewhat whiter light. A capacitor in the circuit contributes to producing a 90 per cent power factor. The maximum over-all length of the lamp is $57^{1}/_{2}$ inches. The length of light source is about 52 inches. The diameter of the luminous part of the tube is $1^{1}/_{2}$ inches. The lamp has a cathode and a pair of anodes. There is a flow of current through the lamp, first, for a half-cycle from one anode and then for the other half-cycle from the other anode to the cathode. Starting voltage is approximately $2^{1}/_{2}$ times the normal operating voltage. There is a delay in starting for a few seconds in order to permit the cathode to attain emitting temperature. A bimetal delay element protects the lamp until the cathode attains satisfactory temperature. It is understood that manufacture of fixtures for these lamps is suspended for the duration.

HIGH VOLTAGE-COLD-CATHODE FLUORESCENT LAMPS

The high-voltage cold-cathode fluorescent lamps[33] are the immediate outgrowth of gaseous-discharge sign equipment developed in recent years as small-diameter tubes, filled first with neon and then with mercury or argon or helium to produce light of different colors for display purposes. They have been evolved out of outdoor sign-lighting practices. The availability of efficient phosphors for lining the long small-diameter tubes employed in sign work made it practicable to utilize such tubes in providing general illumination.

Electrical Features. These tubes are operated upon transformers providing secondary voltages up to 15,000, sufficient voltage being provided for striking the arc. The transformers usually are of 60- to 120-milliampere capacity. Relatively large electrodes are used, usually in the form of metal cylinders. These operate usually at about 150 degrees centigrade. The starting voltage is perhaps $1^{1}/_{2}$ times the operating voltage.

There is a drop of about 130 volts per pair of electrodes operating at 100 to 120 milliamperes. The current must be kept low in order that the watts loss at electrodes may not be excessive. Yet for satisfactory efficiencies one manufacturer recommends that the 25-millimeter tubes should not operate at less than 100 milliamperes, and the 20-millimeter tubes at not less than 90 milliamperes.

As the voltage drop at the electrodes of long tubes is no greater than that of short tubes, recourse is made to long tubes and high voltages where attainment of good efficiency is sought. At a given current the voltage drop for a tube of large diameter is smaller than for a tube of smaller diameter. In the interests of efficiency the diameter of the tube is made small in order that the current per unit of cross section may approach the optimum. These relatively high voltages for starting and for operation formerly were frowned upon for indoor service. However, subject to precautionary restrictions, they are permitted in the National Electrical Code, with the result that high-voltage cold-cathode fluorescent lighting has entered quite extensively into industrial- and commercial-lighting service. Whether or not required, it would seem to be desirable to install some form of automatic circuit breaker on the primary side of the transformer to operate in case the lamp circuit be opened.

The cold-cathode high-voltage lamps start instantly without need for preheating devices.

High-voltage cold-cathode fluorescent lamps generally have been assembled in the installation, with total length of tubing adapted to the space to be illuminated. Tubes lined with fluorescent powders have been welded and assembled as required. Often there has been reliance on the ceiling as a reflector. This sometimes presents an appearance that is an improvement over that of fluorescent units having their own reflectors. But it affords less ease of cleaning and sacrifices light redirection in most cases.

These long slender tubes lend themselves readily to a wide field of custom construction. With the skill developed in sign work the manufacturers may be expected to show facility in providing a variety of shapes and sizes of tubes for special purposes, including conformity with architectural contours.

Performance. Custom assembly of tubes from various sources is subject to variables that make it difficult to arrive at typical performance statistics. There has been very little standardization of a kind that makes possible precise statements as to efficiencies. These may vary through a considerable range, being involved with extent of trans-

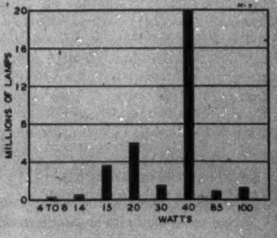

Figure 7. Hot-cathode fluorescent lamps produced in the United States in 1942

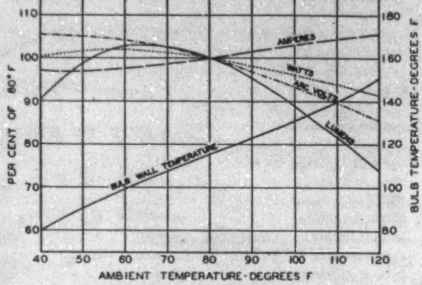

Figure 8. Effect of ambient temperature on characteristics of hot-cathode fluorescent lamps

former loss, voltage, and current. Best efficiency of light production, other things being equal, is found in longer lamps.

Among the most commented-upon installations is that of the I-T-E plant in Philadelphia, in which, after a period of operation, light was found to be produced at a rate of about 266 lumens per foot from tubes operating at about 100 milliamperes. The efficiency, including a 12,000-volt transformer loss, was about 39 lumens per watt. An average of about 35 foot-candles of illumination was produced.

Generally speaking, the brightness of these tubes is of about the same order as that of 40-watt low-voltage hot-cathode lamps. The light produced per foot is about one half, and the projected area of the smaller-diameter tubes is less.

The length of life is dependent somewhat upon the current density, but in practice has been found to be very long—several times that of the life of the low-voltage hot-cathode lamps. Whether this long life is or is not an advantage is a question. It reduces maintenance cost, but the very lack of maintenance may accelerate the rate of decline of useful light output. That decline, aside from dust obstruction, as in the case of all mercury-vapor lamps, is something to be considered. It may result after a long life in a lumen output reduced to two thirds of initial. This will raise a question of an economical "smashing point" before expiration of normal hours life.

A Standard Unit. The recently organized Fluorescent Lighting Association is engaged in standardizing cold-cathode units that can be produced in complete form in factories and shipped ready for installation. These include an eight-foot unit, the luminous part of which is about seven feet, nine inches, in length. The tubes are 20 or 25 millimeters in diameter. These standard tubes may be connected in series on one transformer with power-factor correction in any number of tubes from 2 to 12. Twelve of these 93-inch tubes will operate on a 12,000-volt transformer, the operating voltage being approximately two thirds of the striking voltage. There are other tubes being standardized of the following sizes:

84-inch, known as the seven-foot lamp
76-inch, known as the six-foot lamp
52-inch, known as the four-foot lamp

Some manufacturers are understood to be developing a system of ballast and pairs of cold-cathode lamps utilizing the same lengths of lamps as for the series arrangement, but perhaps adding an 18-millimeter tube in the 76-inch lamp.[34] These will be corrected both as to power factor and flicker. The different lengths of lamps will not differ much in light output but will differ principally in brightness.

General Considerations

ILLUMINATING ENGINEERING

After the war the engineer will have at his disposal a rich assortment of illuminants with which to contribute to the efficiency of industry and business, to the safety of traffic, to the beauty of decoration, and to the comforts of living. Abundant low-cost reliable sources offer favorable opportunity for accelerated development of the application of light. Illuminating engineering practiced as a peaceful art is one of those enterprises that offers advantage for everybody affected—the engineer, the manufacturer and distributor, the supplier of power, and, most important, the public. It is therefore a favorable circumstance that so many varied and effective sources of light are or will be available.

PROPRIETY IN REPRESENTATIONS

Some of the effects of radiant energy are not easily ascertained or measured. They are involved in recondite knowledge that is obscure to the public. The public easily may be confused as to these effects and is likely to be misled by glib representations having a pseudoscientific appearance. For instance, it is possible to represent almost any different kind of radiation that affects the visual organs as being "easier on the eyes" and to get a certain segment of the public to acquiesce in the assertion and to reassert it quite irrespective of the facts. Invisible radiation is even more incomprehensible to the public. It shares with real medicines and quack nostrums the influence of the faith curve. If it be represented to the public as a valuable prophylactic or therapeutic agent applied in certain cases of disease, some part of the public will accept the statement and feel convinced that its experience has confirmed the assertion, quite irrespective of the facts of the case.

Electric energy converted into radiant energy is rendering immense services to the public. It is likely to be more rather than less serviceable in the future. Strong representations as to its value and usefulness may be made with full propriety and with public benefit. But misrepresentations can only impede its growth in usefulness.

Those who produce light sources are among the industrialists and engineers who have had to forsake the ways of peace to put down the threat of aggressor nations against our way of life. When this menace shall be destroyed, they will turn back to the further development of light sources. Then if care is not taken, they may find the situation confused by misrepresentation in these matters that the public does not understand.

It appears to devolve upon engineers, both as an opportunity and as a duty, to do everything that is practicable to encourage valid significant representations regarding light sources in order to afford the public best practicable guidance. The engineering profession and the lighting industry ought to earn and safekeep a high degree of public confidence in its assertions. Means should be found for discriminating between valid reasonable representations and those that mislead.

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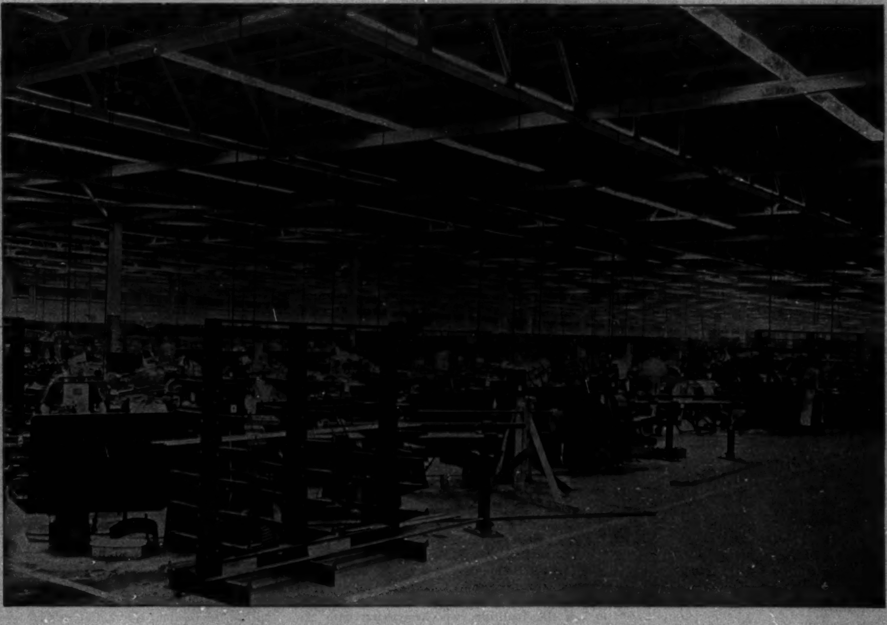

Continuous rows of twin 40-watt fluorescent units illuminate this modern industrial plant to a level of 30 foot-candles. Mounting height is 22 feet and spacing between rows 13 1/2 feet

Report of the Board of Directors

THE BOARD OF DIRECTORS of the American Institute of Electrical Engineers presents herewith to the membership its 60th annual report, for the fiscal year ending April 30, 1944. A general balance sheet showing the condition of the Institute's finances on April 30, 1944, together with other detailed financial statements, is included herein. This report contains a brief summary of the principal activities of the Institute during the year, more detailed information having been published from month to month in *Electrical Engineering*.

BOARD OF DIRECTORS MEETINGS

The board of directors held four meetings during the year, three in New York, N. Y., and one in Cleveland, Ohio. Two executive committee meetings were held.

Information regarding many of the more important activities of the Institute which have been under consideration by the board of directors and the committees is published each month in the section of *Electrical Engineering* devoted to Institute activities.

WAR ACTIVITIES

The Institute has continued to co-operate as fully as possible in undertakings connected with the war efforts, in accordance with the policies previously adopted.

In the programs of national and District meetings, major emphasis has been placed upon technical papers closely related to war efforts. Many sessions and conferences in these meetings have been devoted to such topics as substitution of materials, defense lighting, electric equipment for military aircraft, emergency loading of transmission systems, distribution systems in wartime, problems of engineering education, and technical personnel in wartime.

Institute committees have prepared many guides and reports covering the economical application and operation of electrical apparatus under emergency conditions.

The extent and variety of types of assistance performed by the Institute are shown in a chart on page 413 of *Electrical Engineering* for September 1943, under the title, "AIEE Wartime Engineering Activities Outlined."

PLACES VISITED BY PRESIDENT FUNK

Connecticut
Connecticut Section, New Haven

Florida
Florida Section, Jacksonville

Georgia
Georgia Section, Atlanta

Illinois
Chicago Section
Urbana Section

Indiana
Central Indiana Section, Indianapolis
Fort Wayne Section

Kansas
Wichita Section

Louisiana
New Orleans Section

Maryland
Maryland Section, Baltimore

Massachusetts
Boston Section
Lynn Section
Pittsfield Section
Springfield Section
North Eastern District technical meeting, Boston

Minnesota
Minnesota Section, Minneapolis

Missouri
Kansas City Section

Nebraska
Nebraska Section, Omaha

New York
Ithaca Section
New York Section
Niagara Frontier Section, Buffalo
Rochester Section
Schenectady Section
Winter technical meeting, New York

North Carolina
North Carolina Section, Raleigh

Ohio
Cincinnati Section
Cleveland Section
Columbus Section
Dayton Section
Mansfield Section
Toledo Section

Pennsylvania
Erie Section
Lehigh Valley Section, Bethlehem
Philadelphia Section
Pittsburgh Section
Sharon Section

Rhode Island
Providence Section

South Carolina
South Carolina Section, Columbia

Tennessee
East Tennessee Section {Chattanooga
Knoxville

Texas
Houston Section
North Texas Section, Dallas
South Texas Section, San Antonio

Utah
Pacific Coast technical meeting, Salt Lake City

Virginia
Southern District technical meeting, Roanoke

Wisconsin
Madison Section

Canada
Montreal Section

ANNUAL MEETING

The annual business meeting of the Institute was held Tuesday morning, June 22, 1943. The annual report of the board of directors for the fiscal year which ended April 30, 1943, was presented in abstract by the national secretary. During this meeting a token of appreciation, in the form of an engraved certificate, was presented to National Treasurer W. I. Slichter, in recognition of his long and active service to the Institute. The occasion was the 25th anniversary of his election as an officer of AIEE. National Treasurer Slichter gave a report on the finances of the Institute. The report of the committee of tellers upon the election of officers for the year beginning August 1, 1943, was presented, and President-Elect Funk responded to his introduction with a brief address.

An unannounced item of the meeting was the introduction of Colonel John Millis (A '84, M '85), senior surviving member of the Institute, who responded with appropriate remarks. Howard Whipple Green, director of real-property inventory, city of Cleveland, gave an address on "The Fact Basis for Community Planning." National prizes for papers presented in 1942 were awarded, and the Lamme Medal for 1942 was presented to Joseph Slepian, associate director of research, Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa. The meeting was concluded with the presidential address by H. S. Osborne, entitled "Planning for Things to Come."

NATIONAL TECHNICAL MEETINGS

Three national technical meetings were held during the year, and a brief report on each follows:

Summer Technical Meeting. The 59th summer technical meeting was held in Cleveland, Ohio, June 21–25, 1943. In addition to the annual business meeting and the conference of officers, delegates, and members, there were 26 sessions and conferences with approximately 80 technical papers and informal conference presentations. At the general session, attention was focused on the subject "The Engineer's Relation to Inter-American Affairs." Feature addresses were delivered by Major General Julian L. Schley, director of transportation for the Office of the Coordinator of Inter-American Affairs, and R. E. Zimmerman, vice-president in charge of research and technology, United States Steel Corporation. A dinner conference was sponsored by the AIEE and Cleveland Section, Institute of Radio Engineers, at which Raymond Guy, National Broadcasting Company, gave an address entitled "Frequency Modulation Versus Amplitude Modulation at High Frequencies." Because of wartime restrictions, only two inspection trips, both directly associated with technical conferences, were held. The registration for the summer technical meeting was 1,170.

Pacific Coast Technical Meeting. The 31st Pacific Coast technical meeting was held in Salt Lake City, Utah, September 2–4, 1943, with a registration of approximately 200. There were five technical sessions, five technical conferences, and a student luncheon conference at which N. E. Funk, president, AIEE, gave a brief address, and H. H. Henline, national secretary, spoke on the organization and work of Student Branches. There were inspection trips to the United States Bureau of Mines and the University of Utah; an organ recital; and a get-together dinner, followed by an illustrated lecture, "Exploring Your Back Yard," by R. E. Marsell, professor of geology, University of Utah.

Winter Technical Meeting. The 32nd winter technical meeting was held in New York, January 24–28, 1944, with a program including 14 technical sessions and 12 conferences, at which 69 papers were presented. At a general session an address was given by Willard Chevalier, publisher of *Business Week* and vice-president of McGraw-Hill Publishing Company, Inc., entitled "The Engineer in America's Business Future." During this session, the Edison Medal was presented to Vannevar Bush, president, Carnegie Institution of Washington, D. C., and director of the Office of Scientific Research and Development.

There was an inspection trip to the New York Navy Yard, under the direction of Captain H. G. Rickover, head, electrical section, Bureau of Ships, for especially selected and invited teaching personnel from colleges in which Navy Specialized Training Programs were being conducted. Captain Rickover addressed this group at a closed meeting the following day, and at an open evening meeting spoke on "Electricity on Shipboard." These meetings were sponsored by the AIEE committee on education and the training division of the Bureau of Naval Personnel. A smoker constituted the only entertainment event. The registration for the winter technical meeting was 1,658.

The principal speaker at the joint AIEE–IRE evening session was Major General Roger B. Colton, Signal Corps, United States Army, who discussed "Enemy Army Communications Equipment." Supplementing General Colton's address, there was an exhibit of captured enemy equipment and United States equipment on display during the latter half of the meeting week.

DISTRICT TECHNICAL MEETINGS

Southern District Technical Meeting. This meeting was held in Roanoke, Va., November 16–18, 1943, and included four technical sessions at which 13 papers were presented; a general session with an address by President Funk; two evening sessions, one of which was a dinner meeting; and a student conference with three papers by students. At the dinner meeting, Lieutenant Colonel M. P. Chadwick, Signal Corps, and Captain H. G. Rickover, Bureau of Ships, were the speakers. At the other evening meeting, E. H. Alexander, General Electric Company, Schenectady, N. Y., spoke on "Electronics in Industry." The attendance at the Southern District meeting was 220.

North Eastern District Technical Meeting. At this meeting, held April 19–20, 1944, there were seven sessions with 40 technical papers and a student session. The 60th anniversary of the Institute was observed at a special session with addresses by President Funk and David C. Prince, vice-president, General Electric Company. Edward J. Foley, of the American Air Lines, postwar committee, spoke on "The Future of Aeronautics." Following the presidential reception and get-together dinner, William S. Newell, president, Bath Iron Works, gave an address entitled "Some Thoughts on the Future." The attendance at this District meeting was 630.

SECTIONS

1. Sections Programs. The Sections have been generally following a program of carrying on a reasonable amount of activity, helping with the war effort wherever possible, and keeping in mind the educational and other values inherent in these activities. Considerable effort has been made to retain the diversified interest of the older members, as well as to attract the new and younger members. All Sections have a keen appreciation of the importance of the continuation of the war effort. A great many programs have been devoted to air transportation, various phases of instrument manufacture and their application, the theory and application of electronics, communications, conservation of critical materials, production clinics, and other subjects connected with the war.

2. Development of Technical Groups. As part of the plan to retain the diversified interest of members, the Sections committee prepared a folder, "Technical Groups," which was distributed to all Section officers on January 10, 1944. The Section committee also is making individual contacts with certain Sections where there is a possibility of the successful operation of technical groups. The possibility of forming groups on air transportation, electronics, and instruments and measurements is being encouraged in this manner. The value of such technical groups is being more generally recognized. At the present time, there are 27 technical groups in active operation, and several Sections are considering the advisability of forming other groups.

3. Subsections. One of the most valuable means of accomplishing the objective of the Institute is to provide meeting places at which papers on electrical-engineering subjects can be presented and discussed. Subsection operation provides this opportunity in localities distant from the regular Section meeting places, especially necessary in these times of increasing transportation difficulties. Subsection operation is gaining in popularity since the distribution of the folder, "Subsections," in March 1943. The Sections committee is individually considering the possibilities of 26 locations in the territories of these Sections where there are possibilities of successful Subsection operation. At the present time, there are six Subsections in active operation and two Subsections in process of being formed.

4. Section Territory in the United States and Canada. Upon the recommendation of the Sections committee, the vice-presidents of the Southern District 4; the South West District 7; and the Memphis, North Texas, and Tulsa Sections; the unassigned territory in the State of Arkansas was allocated to these Sections. These changes completed the allocation of all unassigned territory in the United States.

Upon the recommendation of the vice-president of Canadian District 10 and the officers of the Section, the Midwestern Canada Section (formerly Saskatchewan Section) was removed from the list of operating Sections of the Institute in view of the inactivity of this Section due to unusual conditions.

A plan is now under consideration for allocating seven counties in the states of Indiana and Michigan in the territories of the Chicago, Fort Wayne, and Michigan Sections to the South Bend Section, in order to provide the South Bend Section with a more balanced territory.

The assignment of the territory of the Maritime Provinces in Canada of New Brunswick, Nova Scotia, and Prince Edward Island to the Montreal Section is also under consideration.

5. Co-operation With Student Branches. Development of Student Branches at the colleges and universities has been a source of great strength to the Institute. Co-operation of the Sections with the Student Branches was urged in a seven-point program sent to the officers of all Sections and Branches in a joint letter of January 2, 1944, by the committee on Student Branches, committee on education, and the Sections committee.

6. Enhancement of the Professional Status of the Engineer. A Sections committee letter dated April 8, 1944, wa sent to all Sections, including the recently published Engineers' Council for Professional Development pamphlet, "Will You Help?" This literature presents a plan for effectively bringing the ECPD program to the individual engineer through the existing local chapters of the engineering organizations.

7. Civic Affairs. The engineer is frequently criticized for not playing a leading part in the social, civic, and political affairs in his community. While these problems are often considered to be only within the sphere of the civil engineer, the electrical engineer is also vitally affected and should be greatly interested both technically and as a citizen. Postwar-planning activities are tending to draw the engineer more and more into civic affairs of his community. The Sections committee letter of March 6, 1944, to all Section officers outlined the possible methods by which the Sections of the Institute may contribute most effectively to the civic affairs of their local community. Attention was called to the fact that the objectives of the Institute are technical in nature, and the suggested program of civic affairs is necessarily supplemental to the main objectives of the Institute.

8. Section Papers for Publication in "Electrical Engineering." Papers presented at meetings of the Institute's Sections, Subsections, or technical groups provide an excellent source for articles in the general-interest section of *Electrical Engineering.* Publication of desirable papers makes available to the entire Institute membership much excellent information which otherwise would reach only one of its local groups. A Sections committee letter dated April 28,

Table I. Section and Branch Statistics

	For Fiscal Year Ending April 30					
	1939	1940	1941	1942	1943	1944
Sections						
Number of Sections	67	70	72	72	73	73
Number of meetings held	635	701	703	647	598	738
Total attendance	85,692	91,949	92,554	78,254	66,111	83,120
Branches						
Number of Branches	120	121	123	124	125	125
Number of meetings held	1,190	1,346	1,163	946	942	755
Total attendance	53,380	64,972	52,285	37,785	38,227	24,768

1944, points out the desirability of Section officers taking the necessary action to submit more of their good local papers both for publication in *Electrical Engineering* and for entering national and District prize competitions.

9. Use of Identification Cards. Identification cards or badges are now being used by some Sections of the Institute in connection with Section meetings. A Sections committee letter of March 21, 1944, points out the advantages in the use of identification cards or badges and suggests that a more general use of them is desirable at all Section and group meetings.

10. Section Operation and Management. Plans have been developed for three clinics, with parallel meetings for the Section officers and delegates on Section operation and management problems at the summer technical meeting to be held in St. Louis, Mo. Plans also are being made to devote a general session of officers, delegates, and members in St. Louis to the common operating and management problems of all Sections.

A display of Section-meeting notices is also planned at the summer technical meeting in St. Louis.

A simplified report on Section activities is being prepared for distribution to the Sections at the close of the season's activities. The information from these reports will be consolidated and distributed to the Sections. These reports will provide an active analysis of major Section activities for the information of new officers at the beginning of their term of office and should be an inspiration to new Section officers in laying out their Section program for the year.

STUDENT BRANCHES

The Student Branches have been modifying their procedures and instituting new ideas in order to continue their activities under constantly changing conditions due to the war.

In most cases, the Branches have been able to continue with somewhat fewer meetings, and some of the Branches have increased materially their activities and membership. Some of the Districts have been able to continue their sessions with student papers, and nearly all Districts have continued the general conferences on student activities.

The directors of the training programs for both the Army and the Navy have extended their co-operation to the professional activities of the engineering societies which have been conducted through the Branches. The extent of the actual participation by Army and Navy students has varied with local conditions and the understanding of the commandant in charge.

A joint committee on student activities of the several engineering societies has co-operated in presenting the problems of the Student Branches to the Army and Navy and has urged the close co-operation among the Branches of the several societies on each campus with each other.

Committees of the ECPD and Society for the Promotion of Engineering Education have taken the initiative in promoting the solidarity and high standing of the engineering profession through the Student Branches of the several engineering societies. They have been working with the committees on Student Branches, and it is hoped that through the contact with the engineers in training much can be accomplished.

Table II. Statistics of Technical Programs for Last Five Years

	For Fiscal Year Ending April 30				
	1944	1943	1942	1941	1940
Number of national technical meetings	3	3	3	3	2
Number of District technical meetings	2	2	3	2	3
Registration at national meetings and District meetings	3,880	3,497	4,274	4,339	3,548
Number of papers presented	196	168	182	195	177
Number of papers recommended for *Transactions*	153	141	152	167	151
Estimated number of pages required for printing papers in *Transactions*	875*	886*	807*	932*	886*
Average length of papers recommended for *Transactions*	5.72	6.28	5.3	5.72	5.86
Number of technical sessions	53	44	50	46	43
Number of technical conferences	24	24	23	18	10

* Partly estimated.

The Sections committee has urged each Section to have a committee on student activities which would facilitate contacts between the Section and neighboring Student Branches.

EMPLOYMENT CONDITIONS

At its January meeting, the board of directors voted "that the president be authorized to appoint a committee to study broadly the conditions affecting security of employment and compensation among electrical engineers, with special reference to problems of collective bargaining; that this committee be requested to collaborate with a corresponding committee of The American Society of Mechanical Engineers if such a committee is appointed; that the committee be authorized to employ a labor-relations adviser, if it considers this advisable; and that it be granted an appropriation of $1,000."

General Committees

FINANCE COMMITTEE

A substantial increase in revenue from advertising in *Electrical Engineering*, together with additional dues income as a result of a steady growth in Institute membership, are the principal factors accounting for the excess of income over expenditures during the past fiscal year, as shown in the detailed financial statements which appear on the closing pages of this report.

The continued increase in membership has served to offset any loss in dues revenue resulting from concessions being made to members serving in the Armed Forces and merchant marine. As of April 30, 1944, inactive membership status has been arranged for 1,835 such members, as compared to a total of 771 on April 30, 1943.

Expenditures have been in accordance with the program of Institute activities called for in the budget adopted by the Institute board of directors. The favorable condition of finances also has permitted inauguration of a retirement system for members of the headquarters staff through the required initial payment ($85,000) estimated to cover the prior service of such persons. In addition, a transfer of $40,000 was made to the reserve capital fund.

As shown in the balance sheet and schedule 1, Institute investments now have a market value almost equal to the purchase price after making due allowance for losses in securities which developed in less favorable years. Approximately 65 per cent of the investments for the reserve capital fund is now in United States Government bonds.

The policy followed for many years of budgeting Institute expenses within the total of anticipated revenue is the main reason for the Institute's healthy financial condition. The transfer of excess income to the reserve fund should prove to be equally sound policy with respect to providing for those years when Institute income may be impaired by reason of economic developments which cannot be foreseen at this time.

TECHNICAL PROGRAM COMMITTEE

Technical Meetings. Three national technical meetings and two District technical meetings were held during the year. The total registration at the five meetings was 3,880, which exceeded the total registration of 3,497 for the five meetings of the previous year. More technical papers were presented and more technical sessions were held than at any other time during the past five years. The average length of papers recommended for *Transactions* has been lowered from the high of 6.28 pages per paper to 5.72 pages per paper. Statistics of the technical programs for the last five years are given in Table II.

Wartime Programs. In accordance with the wartime policy adopted by the board of directors, preference has been given to papers which aid in the war effort. Many of the papers dealt with the rating and performance of electrical equipment, as well as the application of capacitors and the switching of shunt capacitor banks to get the most out of existing apparatus and equipment. One of the transportation sessions treated the applications of electric locomotives and equipment to open-pit copper-mining operations.

A significant feature was the development of interest in programs devoted to electric equipment for military aircraft and the problems incidental to design and performance at high altitudes. An indication of interest in this subject is shown by the fact that 19.6 per cent of the papers recommended for the *Transactions* were in the field of air transportation.

During the winter technical meeting two important evening sessions were held. In a session held jointly with the Institute of Radio Engineers an address was given by

Major General Roger B. Colton, chief, engineering and technical service, Signal Corps, United States Army, on "Enemy Army Communications Equipment." Through the courtesy of the Signal Corps, an extensive collection of captured German, Japanese, and Italian equipment was displayed. On another evening, an address, "Electricity on Shipboard," was given by Captain H. G. Rickover, head of the electrical section, Bureau of Ships, United States Navy. A special meeting also was held on the Navy *V*-12 Program, which was open only to specially selected and invited guests of the Navy Department.

Table III. Membership Statistics for Fiscal Year Ending April 30, 1944

	Honorary Members	Fellows	Members	Six-Year Associates	Associates	Subtotals	Totals
Membership April 30, 1943	6	823	5,188	6,437	7,707		20,161
Additions							
New members qualified		3	180		1,789	1,972	
Former members reinstated or re-elected		2	111	63	125	301	
Subtotals		5	291	63	1,914	2,273	
Transfers		64	356	829		1,249	
Totals		69	647	892	1,914	3,522	
Deductions							
Died		17	40	41	18	116	
Resigned			25	55	55	135	
Dropped		3	74	261	438	776	
Subtotals		20	139	357	511	1,027	
Transfers			64	313	872	1,249	
Totals		20	203	670	1,383	2,276	
Net changes		49	444	222	531	1,246	
Membership April 30, 1944	6	872	5,632	6,659	8,238		21,407

General Sessions. The general sessions have continued to deal with the broader aspects of the profession as related to the war effort and its impact on engineering work. The general session for the meeting in Cleveland was devoted to "The Engineer's Relation to Inter-American Affairs" and "Inter-American Co-operation in the Development of Standards." The general session for the winter meeting was on the subject of "The Engineer in America's Business Future."

Committee Policies. In continuing the policy of the previous year, to economize on time and travel, only two committee meetings were held. These were attended by 76.6 per cent and 73.3 per cent of the committee members or duly appointed representatives, as compared with the attendance of 56, 50, and 66 per cent for the three meetings during the previous year. Much of the committee work necessarily has been conducted through correspondence.

The committee has appointed a subcommittee on adequacy of coverage of electrical-engineering field by present technical committees, with a view toward making a study to see that rapidly expanding fields are covered adequately. A report and recommendations for the organization of new technical committees, if necessary, will be made later. A joint technical-program–sections subcommittee has been appointed in co-operation with the chairman of the sections committee to handle matters related to civic affairs.

PUBLICATION COMMITTEE

War problems continued to be given first place in the Institute's principal technical publications during the past year, as in the previous year. In addition, attention was devoted to the postwar period through the organization and development of a special series of articles to appear in *Electrical Engineering*, beginning with the May 1944 issue. Publication procedures remained the same as in the previous year, no change in policy having been adopted.

In spite of continued restrictions on the use of paper, the monthly issues of *Electrical Engineering* carried a substantial number of general-interest articles and a generous proportion of news material. Likewise, the monthly Transactions sections of *Electrical Engineering* included most of the year's approved technical papers, leaving only a few very long papers to be published with the discussions in the two semiannual "Supplements to *Electrical Engineering*—Transactions Section." The 1943 *Transactions* volume contains all approved 1943 technical papers and related discussions.

Beginning January 1, 1944, an additional cut in the use of paper by periodicals was ordered by the War Production Board, the total reduction for the calendar year 1944 amounting to 25 per cent under the amount used during the calendar year 1942. (For 1943, the required reduction was ten per cent under the 1942 figure.) To effect the increased reduction without a serious curtailment of the amount of technical material published, the following changes in format of *Electrical Engineering* were made, beginning with the January issue:

1. The over-all trim size was reduced from $8^{3}/_{4}$ by $11^{5}/_{8}$ inches to $8^{1}/_{8}$ by $11^{1}/_{4}$ inches. The dimensions of the type page were kept substantially the same as before, and thus in effect simply the page margins were reduced.
2. The weight of paper stock was reduced from the former 50-pound basis to 45-pound basis.
3. Minor typographical changes were made to adapt the type page to the new trim size and to make the over-all dimensions of the type pages the same in the general and Transactions sections.

Table IV. Number of Applications Received From Enrolled Students and From All Others

Year Ending April 30	Students	All Others	Total
1944	466	1,908	2,374
1943	783	1,431	2,214
1942	971	1,031	2,002
1941	887	1,011	1,898
1940	911	918	1,829
1939	849	872	1,721

Table V. Number of Enrolled Students as of April 30

Year	New Applications	Renewals	Total
1944	2,242	2,656	4,898
1943	2,512	3,200	5,712
1942	2,585	3,377	5,962
1941	2,351	3,188	5,539
1940	2,525	2,992	5,517
1939	2,271	2,971	5,242

Advance pamphlets of approved technical papers were produced as required by the technical program committee for the national and District meetings held during the year. In addition, two special pamphlets were produced:

1. "Emergency Overloading of Insulated Power Cables," issued in September 1943 on recommendation of the AIEE committee on power transmission and distribution. This pamphlet includes nine papers presented at the conference on emergency rating of power cables held during the 1943 AIEE winter technical meeting, New York, N. Y., under sponsorship of the cable working group of the committee.
2. "Ultrashort Electromagnetic Waves." This is a consolidated reprint of six lectures published in *Electrical Engineering*, originally presented as a symposium under auspices of the basic science group of the New York Section.

In addition to these special publications, a quarterly AIEE *Bulletin* for Institute members in the armed services and the merchant marine was published, beginning in June 1943 This consists of a four-page pamphlet, publication of which was authorized by the board of directors in January 1943. To date it has carried primarily news material concerning the Institute and related activities considered to be of interest

Table VI. Record of AIEE Membership

Year	Total May 1	Year	Total May 1	Year	Total May 1
1884	71	1905	3,460	1925	17,319
1885	209	1906	3,870	1926	18,158
1886	250	1907	4,521	1927	18,344
1887	314	1908	5,674	1928	18,265
1889	333	1909	6,400	1929	18,133
1890	427	1910	6,681	1930	18,003
1891	541	1911	7,117	1931	18,334
1892	615	1912	7,459	1932	17,550
1893	673	1913	7,654	1933	17,019
1894	800	1914	7,876	1934	15,230
1895	944	1915	8,054	1935	14,269
1896	1,035	1916	8,202	1936	14,600
1897	1,073	1917	8,710	1937	15,308
1898	1,098	1918	9,282	1938	16,078
1899	1,133	1919	10,352	1939	16,605
1900	1,183	1920	11,345	1940	17,213
1901	1,260	1921	13,215	1941	17,886
1902	1,549	1922	14,263	1942	18,944
1903	2,229	1923	15,298	1943	20,161
1904	3,027	1924	16,455	1944	21,407

to members in the services. Some 2,000 copies now are being produced and distributed.

Governmental censorship regulations have continued in effect as reported last year, although regulations have been relaxed in some respects. The procedure for exporting publications, however, was changed. Under former procedure, an export license had to be obtained from the Office of Export Control of the Board of Economic Warfare for each issue of each publication shipped to foreign destinations (except Canada). Under the new procedure, export licenses no longer are required; foreign shipments now are subject to the regulation censorship scrutiny, but the procedure is facilitated by submitting advance proofs to the district postal censor in New York. Full co-operation has been extended to, and has been received from, the Office of Censorship.

MEMBERSHIP COMMITTEE

The Institute membership stood at 21,407 as of April 30, 1944. This compares with a membership of 20,161 on April 30 of last year. The net increase in membership for the past two years amounts to nearly 2,500. Several conditions have helped to bring about this extremely favorable position. The employment of electrical engineers has continued at a high level; the Section meetings have been well planned for the immediate needs; advances in the application of electrical equipment to the war effort have made many engineers realize the need for keeping abreast of new developments.

It is of interest to note that of the total membership on April 30, 1944, there were 1,835 men being carried in an "inactive" status because they were in military service or were located in foreign countries and unable to remit dues at this time. Similarly, 1,288 members are being carried who owe dues for the year ending April 30, 1944. The number of persons now enrolled as Members for Life totals 1,028.

The committee herewith acknowledges the good response to the chairman's annual letter requesting names of qualified candidates. This still continues to be the best source for new prospects and we are grateful to those members who took the time to send in names. There are undoubtedly many instances of personal contacts which result in new members that never come to our attention. This effort is also here acknowledged.

It has been our custom to include several tables to show more conveniently what is happening to Institute membership. This practice is continued again this year on a reduced scale which excludes some exhibits no longer deemed useful.

In Table III it is seen that the gross additions to membership for the past year were 2,273, while the deductions were 1,027. The net increase in membership was 1,246.

Table IV shows the continuing decline in applications from Enrolled Students and a continuing substantial upward trend in applications from others. This condition is likely to continue throughout the war period. No direct check can be made between this table and the preceding one, because applications received are not all acted on before the close of the fiscal year.

Table V gives information on the number of Enrolled Students and new applications for student membership from students. While there has been a falling off during the last two years, the change has not been so great as might have been expected.

Table VII. Deaths of AIEE Members Reported in "Electrical Engineering"

Name	Date of Election	Date of Death	Grade at Death	Obituary Notice in "Electrical Engineering"
Anderegg, G. A.	A '04	Aug 14, '43	F	Oct '43, p 463
Anthony, R. H.	A '17	May 1, '43	A	July '43, p 319
Bang, C. M.	A '21	Oct 14, '42	A	Aug '43, p 378
Bauerle, J. L.	A '37	Sept 25, '43	A	Dec '43, p 551
Beckett, Wm.	M '26	Dec 11, '43	M	Apr '44, p 149
Beebe, M. C.	A '98	Nov 28, '43	F	Feb '44, p 70
Bellows, B. C.	M '36	Dec 22, '43	M	Feb '44, p 70
Bender, A. P.	A '18	Oct 22, '43	A	Feb. '44, p 71
Blackwell, F. O.	A '00	Apr 11, '43	M	June '43, p 275
Boesch, J. E.	A '08	July 7, '43	M	Oct '43, p 464
Boileau, W. E.	A '94	Aug 3, '42	M	May '43, p 220
Bourne, Frank	A '91	Feb 1943	M	May '43, p 221
Boyer, E. E.	A '95	Feb 9, '44	F	Apr '44, p 150
Brizey, R. de W.	A '08	Mar 14, '43	A	May '43, p 221
Brobst, J. E.	A '19	Sept 30, '43	A	Nov '43, p 508
Brockhofl, F. W.	A '38		A	Dec '43, p 550
Bullen, D. R.	A '02	May 10, '43	A	June '43, p 275
Carter, R. A.	A '09	Apr 2, '43	A	June '43, p 274
Chadwick, R. H.	A '17	May 29, '43	M	July '43, p 319
Cochrane, J. W.	A '42	Nov 6, '43	A	Jan '44, p 32
Cortright, T. R.	A '43	June 22, '43	A	Nov '43, p 508
Darlington, F.	A '02	Oct 27, '43	F	Dec '43, p 550
Davis, W. J.	A '95	Feb 18, '44	M	Apr '44, p 149
Dodson, J. W.	A '38	May 12, '43	A	July '43, p 319
Dommerque, F. J.	A '94	Dec 7, '42	F	Aug '43, p 378
Douglass, W. E.	M '37	Nov 11, '42	M	July '43, p 319
Eckles, C. E.	A '30	Nov 16, '43	A	Feb '44, p 71
Edwards, C. V.	A '99	Oct 2, '43	A	Dec '43, p 551
Elden, L. L.	A '03	Oct 21, '43	M	Dec '43, p 550
Engelken, R. C.	M '33	Jan 19, '44	M	Mar '44, p 111
Everit, E. H.	A '02	Oct 5, '43	F	Apr '44, p 149
Falkenwald, C. O.	A '40	Mar 25, '43	A	May '43, p 221
Feiring, F. G.	M '43	Sept 19, '43	M	Dec '43, p 550
Foster, Wm. J.	A '07	July 2, '43	F	Aug '43, p 377
Freeman, F. S.	A '16		M	Nov '43, p 508
Gaylord, J. C.	A '11	June 2, '43	M	Aug '43, p 378
Gill, P. C.	A '24	Apr 25, '43	M	July '43, p 319
Grace, I. W.	A '19	June 2, '43	A	Sept '43, p 416
Grimes, W. F.	A '19	June 26, '43	M	Sept '43, p 416
Hampton, W. O.	M '41	Feb 9, '44	M	Apr '44, p 150
Happy, John	A '40	Feb 10, '43	A	May '43, p 221
Hart, H. C.	M '27	Mar 23, '43	M	June '43, p 275
Hedrick, E. R.	M '25	Feb 3, '43	M	May '43, p 220
Herd, Philip	M '21	Aug 19, '42	M	June '43, p 274
Hilford, J. V.	A' 35	Sept 18, '43	A	Feb '44, p 71
Hines, J. W.	A' 22	Sept 30, '43	A	Dec '43, p 551
Hood, J. E.	A '38		A	Feb '44, p 71
Hunt, F. L.	A '05	Feb 8, '44	F	Apr '44, p 149
Jerauld, R. E.	A '21	Feb 25, '43	A	May '43, p 220
Johnson, J. A.	A '40	Nov 22, '42	A	May '43, p 221
King, H. M.	M '21	Dec 6, '43	M	Apr '44, p 150
Kingsbury, Albert	A '08	June 28, '43	A	Sept '43, p 416
Kirkman, O. C.	A '39	May 21, '43	A	Nov '43, p 508
Korner, A. J.	A '16	Aug 1943	F	Feb '44, p 70
Kreul, A. H.	A '19	May 5, '43	M	July '43, p 319
Larsen, C. J.	A '14	Feb 7, '44	F	Apr '44, p 149
Lee, J. C.	A '90	Dec 16, '42	A	May '43, p 221
MacLellan, T.	A '12	June 12, '43	A	Sept '43, p 417
Magley, A. J.	A '24	Dec 22, '43	A	Mar '44, p 111
Marburg, L. C.	A '02	June 26, '43	F	Aug '43, p 378
McCormick, F. J.	M '37	Nov 1942	M	Aug '43, p 378
McKinley, W. W.	A '38	Nov 10, '43	M	Feb '44, p 70
Merrill, B. M.	A '17	June 15, '43	F	Aug '43, p 378
Mickle, H. I.	A '31	July 27, '43	A	Sept '43, p 417
Millar, T. R.	A '08	Mar 2, '43	M	May '43, p 220
Miller, J. G.	A '41	Dec 1942	A	Dec '43, p 551
Muller, E. W.	A '05	Apr 8, '43	M	July '43, p 318
Murphree, D. D.	A '40	May 4, '43	A	July '43, p 319
Nash, D. O.	A '31	Nov 6, '42	M	July '43, p 319
Newberry, R. C.	A '39	In Action	A	Aug '43, p 379
Ninis, F. D.	A '07	Mar 2, '44	F	Apr '44, p 148
Nitz, H. I.	A '41	Dec 8, '42	A	May '43, p 221
Paleschuck, M. H.	A '43	Dec 28, '43	A	Feb '44, p 71
Perry, J. W.	A '03	Mar 9, '43	M	Aug '43, p 379
Plenge, H. D.	A '24	June 12, '43	A	Dec '43, p 551
Pollock, M. W.	M '31	Dec 9, '42	M	Aug '43, p 378
Potter, D. F.	M '31	Nov 5, '43	M	Feb '44, p 71
Reed, H. D.	A '94	Apr 27, '43	M	June '43, p 275
Reid, F. H.	M '28	Oct 29, '43	M	Jan '44, p 32
Reid, R. E.	A '12	Oct 1942	A	Jan '44, p 32
Reynolds, F. J.	A '20	Dec 19, '43	A	Feb '44, p 71
Rhine, C. P.	A '18	Jan 17, '44	A	Apr. '44, p 150
Roberts, T. M.	A '02	Jan 10, '44	M	Apr '44, p 148
Robinson, J. A.	A '30	May 16, '43	A	July '43, p 319
Robinson, J. P.	A '26	Sept 20, '43	A	Nov '43, p 508
Roddey, J. H.	A '06	Oct 18, '43	M	Dec '43, p 551
Rook, W. E.	A '43	July 24, '43	A	Oct '43, p 464
Schroder, J. H.	M '42	May 3, '43	M	June '43, p 275
Scott, H. G.	A '11	Nov 1, '43	M	Jan '44, p 32
Sharp, K. E.	A '35	May 24, '43	A	Sept '43, p 417
Shaw, H. B.	A '97	Dec 15, '43	A	Feb '44, p 70
Smith, G. B.	A '12	Dec 10, '43	M	Apr '44, p 149
Snyder, E. B.	A '17	Dec 17, '43	M	Mar '44, p 111
Stanley, H. A.	A '07	Oct 18, '43	M	Jan '44, p 32
Stone, J. S.	A '08	May 20, '43	A	July '43, p 318
Stuart, C. E.	M '18	June 20, '43	M	Sept '43, p 416
Sunny, B. E.	A '03	Oct 5, '43	F	Dec '43, p 550
Taylor, N. S.	A '14	Feb 21, '44	M	Apr '44, p 149
Tepel, H. A.	A '23	July 13, '43	A	Dec '43, p 551
Thompson, F. D.	A '07	Feb 3, '44	A	Apr '44, p 150
Warwick, G. G.	A '27		M	Feb '44, p 71
White, H. E.	A '03	Nov 29, '43	A	Feb '44, p 70
Wooldridge, W. J.	A '06	Apr 6, '43	F	Nov '43, p 508
Wyman, W. S.	A '03	Nov 15, '42	F	Sept '43, p 417

Table VI headed "Record of AIEE Membership" is self-explanatory and is included purely for historical purposes.

Table VII listing "Deaths of AIEE Members Reported in *Electrical Engineering*" is regretfully included. It is our way of paying a last humble tribute to those who have taken their departure from our ranks.

In last year's report we promised the membership a booklet concerning the Institute. This was completed and a copy mailed to each member last fall. It is entitled "Accomplishments of the American Institute of Electrical Engineers in the Engineering World" and seems to have enjoyed a good reception. We have had many requests for additional copies, and it is currently being used by the Section membership committees with good results in interesting prospects in joining the Institute.

The Section membership committees have continued to function smoothly and efficiently during the past year and merit our unqualified commendation. It is estimated that about 1,000 members are engaged in this one phase of Institute work.

BOARD OF EXAMINERS

The board of examiners held 12 meetings during the past year, averaging about $2^3/_4$ hours each and acted upon 4,465 cases, divided as shown in table VIII. While total number of cases is somewhat smaller than last year, because of a decrease in both the number of Associate and Enrolled Student applications, the number of cases for both direct admission and transfer to Member and Fellow increased over 28 per cent.

During the preceding year a subcommittee was appointed to study the constitutional requirements for Fellow grade. This committee submitted its report. The committee on constitution and bylaws also submitted a report covering in part the requirements for Fellow. The two reports differed to a considerable extent, particularly from the viewpoint that the constitution and bylaws committee suggested an escape clause applying to the five years as Member requirement. The points at variance are still under discussion.

Table VIII. Applications for Admission and Transfer, 1943–1944

Applications for Admission		
Recommended for grade of Associate	1,461	
Re-elected to the grade of Associate	129	
Not recommended	1	
		1,591
Recommended for grade of Member	235	
Re-elected to the grade of Member	26	
Not recommended	48	
		309
Recommended for grade of Fellow	3	
Not recommended	1	
		4
Applications for Transfer		
Recommended for grade of Member	364	
Not recommended for grade of Member	17	
		381
Recommended for grade of Fellow	70	
Not recommended for grade of Fellow	1	
		71
Students		
Recommended for enrollment as Students		2,109
Total		4,465

COMMITTEE ON CONSTITUTION AND BYLAWS

In keeping with the action of the board of directors at its meeting of October 23, 1942, section 65 of the bylaws was modified by adding to the list of technical committees the committee on electronics. Also, section 68 of the bylaws was modified to include the chairman of the Standards committee as one of the ex-officio members of the technical program committee.

In keeping with the action of the board of directors at its meeting of June 24, 1943, section 30 of the bylaws was modified to remove the state of North Dakota and all of South Dakota except the western portion, now in Denver Section territory, from District 6, and to include them in District 5.

Throughout the year, the committee on constitution and bylaws has been giving consideration to a number of amendments to the constitution, but has delayed submitting its recommendations in order to consider fully certain ideas of the board of examiners with reference to the qualifications of those applying for the Fellow grade. The committee is about ready to make its recommendations to the board of directors.

The committee has under consideration modifications to sections 30, 31, and 67 of the bylaws, and also the matter of modifying our constitution and bylaws to provide for the organization of Institute Sections in territory not on the North American continent. The committee expects to report on all of these matters before the close of the present administrative year.

COMMITTEE ON PLANNING AND CO-ORDINATION

The committee recommended that the president be authorized to appoint a special postwar planning committee.

It recommended the holding of a Northeastern District meeting in Buffalo, N. Y., in the late spring of 1945, and a Southern District meeting in the fall of 1945, the location and dates to be selected later.

In accordance with a recommendation by the joint conference committee, the committee on planning and co-ordination recommended that the Institute appoint a committee of three to serve as Institute representatives on a joint committee of representatives of the engineering societies to study and recommend a plan for the organization of the engineering profession.

The committee considered a proposal referred to it by the board of directors that a new office of vice-president be established to represent foreign members and Sections (except Canada). Its recommendation was that, since the vice-president of District 3 is charged already with the responsibility of representing foreign members, and the Sections committee is engaged in contacts with foreign groups, the objectives back of the proposal be attained, at least during the war, by collaboration of the vice-president of District 3 and the Sections committee.

A proposal that the Institute establish a committee on civic affairs had been referred to the committee by the board of directors for study and report. Since the Sections committee and the technical program committee are interested in this field, the committee on planning and co-ordination recommended the appointment of a joint subcommittee of those two committees, rather than the establishment of a new general committee.

All of these recommendations were approved by the board of directors.

COMMITTEE ON RESEARCH

Because of limitations resulting from the war, the research projects of the Engineering Foundation sponsored by AIEE have been reduced to a single project, namely, the extensive welding-research program sponsored jointly with the American Welding Society. Project 74 on insulating oils and cable saturants, carried on for several years, was terminated at the close of the preceding year, as it was a long-term project not contributing directly to the solution of urgent war problems. The results of this research to date have been particularly valuable, and it is highly probable that this project will be resumed when conditions are suitable.

The committee now is actively considering postwar research problems, so that suitable projects of general benefit can be initiated promptly when conditions are again normal. Because of urgency and secrecy requirements, the Engineering Foundation procedure has been less suited to the conduct of war research than other procedures created to meet the emergency conditions.

The other activity of the committee has consisted in the study of proposed legislation affecting engineering research and electrical engineers. A resolution opposing the passage of the Kilgore bill was recommended to the board of directors and subsequently approved.

COMMITTEE ON SAFETY

During the past year, the committee on safety held two meetings. At the first, on June 22, 1943, during the summer technical meeting in Cleveland the committee meeting was preceded by an open conference at which technical paper 43-44, "Practical Accident Prevention in Small Groups," was presented by O. S. Hockaday. Discussion emphasized the importance of safety being made a definite part of all training programs and pointed out the responsibility of foremen.

The second meeting was held January 26, 1944, at Institute headquarters in New York during the winter technical meeting. At this meeting, a report on the effectiveness of artificial respiration was made in technical paper 44-22 presented by W. B. Kouwenhoven, entitled "Study of Artificial Respiration on Anesthetized Men." At this meeting, the chairman directed attention to certain considerations in connection with the committee's work, and emphasized the importance of its activities being carried on in such manner that there will be no conflict with, but rather support given to, the work of the many other committees and agencies working in the field of electrical safety. The committee will seek opportunities to give service to the AIEE technical committees by directing attention to matters on which joint action may be appropriate. The committee considers it to be incumbent upon it to qualify itself in this manner and to concern itself only with such subjects as will lead to its being recognized as the foremost committee in the field of electrical safety from the technical standpoint.

A communication was sent from the committee at this meeting to the chairman of the conference on AIEE co-operation with other organizations, conveying the wish of the committee on safety to render any service in this connection that might be appropriate.

Continued encouragement and assistance will be given by the committee to the Student Branches in connection with the holding by the Branches of meetings devoted to the discussion of safety subjects. The committee also wishes to direct attention to the desirability of appropriate papers on accident prevention in the electrical field or other allied subjects being included in the programs of District meetings from time to time.

The committee is studying the need for authoritative papers on subjects in or related to the field of electrical safety, and contemplates having any such papers as may be decided upon prepared as promptly as conditions make possible.

On recommendation of the chairman of the committee on safety, the New York Section participated as a co-operating organization in sponsoring the Greater New York Safety Conference and was represented by W. T. Rogers.

Members of the committee will represent the Institute at the annual meeting of the National Fire Protection Association in Philadelphia, Pa., May 8 to 11, 1944.

Standards

STANDARDS COMMITTEE

The increased activity level in Standards work evidenced during the past few years has been maintained fully during the year 1943–44. This applies not only to the completion of projects which have been underway for some time, but also to the initiation of new undertakings. An interesting story could be written concerning each of these items, but in line with the times only a very brief report will be made.

The technical committees have been very active, and the Standards co-ordinating committees (created by the late R. E. Hellmund) have proved particularly effective aids in Institute standardization work. A study of the scopes of the co-ordinating committees is now underway, with a view toward effecting a clearer delineation of their activities.

Among the new AIEE Standards approved during the year were "Guiding Principles for Selection of Reference Values in Electrical Standards" (number 3), "A-C Power Circuit Breakers" (number 19), "Capacitance Potential Devices and Outdoor Coupling Capacitors" (number 31), and a revision of the wartime supplement to "Recommended Practice for Electrical Installations on Shipboard" (number 45*A*).

The wartime guides which were sponsored last year have continued to be very much in demand, and many of them already are out of print.

Projects now in the course of development include "General Service Conditions," "Cable Potheads," "Test Code for Impulse, Low-Frequency, and D-C Testing," "Specifications for Prime-Mover Speed Governors," "Conduction in Stationary Contact Surfaces," and "Standardization of Turbogenerator Characteristics." The turbogenerator work is being carried on jointly with the ASME.

A comprehensive review is being made of the status of all Institute Standards and test codes which have recently been or are now actively being worked on. "Basic Principles for Rating of Electrical Machinery" (1*A*) is under review for adoption as a Standard. The Standards committee also has encouraged and assisted the various technical committees in the preparation of new Standards and revision of Standards.

Copies of all AIEE Standards were sent to the Government of China with a letter pointing out the co-operative advantages of adherence to American Standards. A proposed Chinese standard of voltages and ratings of electric systems and apparatus was circulated for review.

Remarkable strides are being made throughout the industry in the development of electric equipment for aeronautical purposes. Interested groups have been advised that the Institute stands ready and anxious to be of assistance in the preparation of Standards for aeronautical electric equipment, particularly fundamental Standards and standard test methods.

In the field of American Standards Association standardization, several of the sectional committee projects in which the Institute has a very live interest, through sponsorship or joint sponsorship, have made considerable progress, with the Institute's active participation, toward final approval as American Standards. Two such instances to be noted particularly, which have been the centers of considerable controversy, are "Lightning Arresters," *C*62, former AIEE Standard 28, and "Line Insulators," *C*29, former AIEE Standard 41. The sectional committee developing "Graphical Symbols," *Z*32, has also succeeded in clearing up conflicts in a small group of symbols of considerable use in the power, communication, and radio fields. A new American Standard for electronic devices has just been added to their list.

The Standards committee has made recommendations concerning several new members for ASA sectional committees to replace Institute representatives no longer active and has also appointed new representatives where it seemed advisable.

Only three committee meetings have been held so far this year, and much of the work has been handled effectively by correspondence. Another meeting is scheduled for May.

UNITED STATES NATIONAL COMMITTEE OF THE INTERNATIONAL ELECTROTECHNICAL COMMISSION

The International Electrotechnical Commission and its standardization activities during the past year have remained in the suspended state prevailing since the beginning of hostilities in Europe. It is expected that this condition will continue until peace returns.

The United States National Committee, at its annual meeting in December 1943, re-elected the following officers: *President*, E. C. Crittenden; *Vice-President*, L. F. Adams; *Vice-President and treasurer*, H. S. Osborne.

The United States National Committee was honored to have as its guests at its annual meeting Percy Good, secretary of the British National Committee IEC, and director, British Standards Institution; H. A. Grant, secretary of the Canadian National Committee IEC; Colonel W. R. McCaffrey, secretary of the Canadian Engineering Standards Association; and W. P. Dobson of the Canadian Engineering Standards Association.

There was discussion at the meeting of the proposed United Nations standards organization. This organization appeared likely to meet with the approval of the various United Nations for fulfilling the international standardization needs of these nations during the wartime period, and until such time thereafter as the regular international standardization organizations could be reorganized and placed on an operating basis.

Technical Committees

COMMITTEE ON AUTOMATIC STATIONS

The committee held two meetings during the year, one at the summer technical meeting in Cleveland in June 1943, and one at the winter technical meeting in New York in January 1944, at which the work of the committee was discussed.

At the meeting of the committee held in Cleveland, the question of revising AIEE Standard 26 (American Standard *C*37.2), "Automatic Stations," was discussed. The revising of this Standard is now in progress and is expected to be completed this year.

Wartime restrictions and pressure of war work, to a large extent, has limited the amount of new development in automatic-station equipment and supervisory control. The committee's work, therefore, has been concerned largely with operating features of equipments and systems.

Two subcommittee reports were presented at the summer technical meeting in Cleveland: one a report on conductors used for supervisory control, and the other a report on a-c automatic-reclosing equipment applying to stub feeders.

Co-operation With Other Committees. The committee on automatic stations is represented on the joint subcommittee on power system applications of carrier current and the committee on electronics.

By initial action of the committee on instruments and measurements and in co-operation with the committee on automatic stations, a joint subcommittee on telemetering again has been appointed and is comprised of members of both the committee on instruments and measurements and the committee on automatic stations.

The committee has also participated jointly with the stations subcommittee of the committee on power transmission and distribution in preparing a questionnaire to be used in surveying the subject of operating distribution substations, automatic substations, and supervisory controlled substations. This questionnaire has a bearing on postwar activities, and it is felt will be of interest to both operating and manufacturers' engineers.

COMMITTEE ON BASIC SCIENCES

The work of the committee on basic sciences has been limited, because of the time devoted to the war effort by the members themselves and others who normally would be active in preparing papers for publication. Much fundamental work is now in progress all over the country, but only a very limited amount is being written for publication. This trend can be expected to continue.

Some consideration has been given to the holding of a conference on mathematical methods and the solution of equations in engineering work. After consultation with members of the committee and other interested persons, it was decided that this activity could not be conducted at the present time. It is hoped that such a conference can be held some time in the future when the press of war work has eased.

COMMITTEE ON DOMESTIC AND COMMERCIAL APPLICATIONS

The committee has been enlarged to a total of 19 members, and three subcommittees have been formed.

One subcommittee is working on electrical farm applications. It will survey the field and make specific recommendations as to how and what work might be undertaken by the Institute in regard to agricultural electrification. A meeting was held in Chicago, Ill., last fall, and a report is scheduled for presentation at the summer technical meeting.

A second subcommittee is studying domestic and commercial wiring. Efforts are being made to procure technical papers related to this problem.

At the winter technical meeting, a conference was held dealing with topics under the scope of the committee. Tentative plans for the summer technical meeting call for a session or a conference on heating houses electrically. Plans for the future include also a session on wiring of homes and commercial establishments and a session devoted to household appliances with particular emphasis on the influence of their load characteristics on electricity supply facilities. The committee has noted a growing interest in domestic and commercial applications.

COMMITTEE ON ELECTRICAL MACHINERY

The committee held two meetings during the year, one at the summer technical meeting in Cleveland, and one at the winter technical meeting in New York. Papers for national and District meetings were considered, and the work of the committee and the various subcommittees was reviewed.

Subcommittee Activities. The synchronous machinery subcommittee is doing the final editing on the revised test code which has been reviewed by the test code co-ordinating subcommittee.

The transformer and induction regulator subcommittee completed and presented in Cleveland two additional guides, namely, "Interim Report on Overloading Distribution Transformers" and "Interim Report on Overloading Current-Limiting Reactors." The subcommittee initiated papers on impulse testing, effects of altitude, and additional papers on transformer loading.

The induction machinery and the d-c machinery subcommittees sponsored conference sessions at the winter technical meeting in New York on the "Measurement of Temperature Rise" and the "Relation of Hot-Spot Temperatures to Measured Values of Temperature Rise." The induction machinery subcommittee will sponsor at the summer technical meeting in St. Louis a series of conference papers on this same subject, expected to lead to conclusions with regard to possible revisions in the Standards of allowable temperature rise by various methods of measurement.

The insulation resistance subcommittee is still working on the test code for measuring insulation resistance.

During the year a new subcommittee on industrial control was formed. This committee sponsored a conference session at the winter technical meeting and has a number of papers to be presented at the summer technical meeting in St. Louis.

AIEE Technical Meetings. The committee on electrical machinery sponsored three technical sessions at the summer technical meeting in Cleveland, which included three papers on synchronous machinery, three on induction machinery, and three on transformers. At the winter technical meeting in New York, two technical sessions and three conference sessions were sponsored. In the technical sessions there were five papers on induction machinery and four on transformers. There were two conference sessions on the "Measurement of Temperature Rise of A-C and D-C Machines" and one conference session on "Industrial Control."

Co-operation With Other Committees. The committee is represented on the joint committee of the Standards committee on outdoor cable-terminal potheads, roof bushings, and floor and wall bushings, the subcommittee on temperature measurements of the committee on instruments and measurements, the subcommittee of the Standards committee on Standards of ratings of steam-turbine generators, and the subcommittee of the technical program committee to review the scopes of the various technical meetings.

COMMITTEE ON ELECTROCHEMISTRY AND ELECTROMETALLURGY

The committee held one meeting during the year at the winter technical meeting in New York. Plans were made for a session to be sponsored by the committee at the summer technical meeting in St. Louis. Reports from two subcommittees were received and discussed.

The subcommittee on voltage transients in arc-furnace circuits presented a progress report which will be submitted officially after certain changes requested by the members of the subcommittee have been made. It is planned to publish this report as soon as it has been approved. The report contains an excellent summary of a considerable number of tests made at different furnace installations, and a tabulation of results obtained has been made by the data group of the subcommittee.

The subcommittee on metallic rectifiers reported that considerable progress has been made in studying the possibilities of standardizing the nomenclature and the symbols used. The report of Chairman M. F. Skinker indicates that a great deal more work should be done in what is a relatively new field.

Preliminary steps now are being taken in co-operation with the committee on industrial power applications and the committee on electronics to form a joint subcommittee on the heating of nonmetallic materials by radio-frequency power, generally called dielectric heating. The organization of this subcommittee as yet has not been finally determined.

COMMITTEE ON ELECTRONICS

During the year, a subcommittee on X-ray tubes was formed. The scope of this committee is limited to the consideration of X-ray tubes as electronic devices in order to avoid conflicts with the committee on applications of electricity to therapeutics which deals with the medical application of X rays. The new subcommittee will undertake to encourage the publication of papers on X-ray tubes in the *Transactions* and will sponsor conferences at technical meetings. It also will pursue standardization work in its own field.

The subcommittee on electronic standards has appointed a working subcommittee on nomenclature and letter and graphical symbols. This group has underway an active program on definitions, the object of which is to supplement and revise the electronic definitions included in the American Standard Definitions of Electrical Terms.

Publication continues to be restricted because of the war program, but the committee participated in joint sessions at both summer and winter technical meetings.

COMMITTEE ON INDUSTRIAL POWER APPLICATIONS

Throughout the year, the four subcommittees worked intensively on the preparation of a committee report on "Electric Power Distribution for Industrial Plants." Meetings of the co-ordinating committee were held in Pittsburgh, Pa., on December 15, 1943; at the winter technical meeting in New York on January 26, 1944, and in Detroit on March 1 and 2, 1944. The report will be presented at a conference session sponsored by the committee on industrial power applications at the summer technical meeting in St. Louis in June.

At the 1943 summer technical meeting in Cleveland, the committee sponsored, with the committee on electric welding and the committee on power transmission and distribution, a joint conference session on resistance welding and its associated power supply. Five conference papers and a technical paper were presented on this subject. At another session, the committee sponsored jointly with the committee on electronics the presentation of five papers dealing with the applications of electronics in industry. For the Pacific Coast technical meeting in Salt Lake City, Utah, September 2 to 4, 1943, the committee furnished two papers on the design of contacts and electronic control of d-c motors.

At the winter technical meeting in New York, January 24 to 28, 1944, the committee sponsored two technical sessions at which six technical papers and three conference papers were presented.

COMMITTEE ON INSTRUMENTS AND MEASUREMENTS

The work of the committee has been affected materially by war conditions. Necessity for secrecy and pressure of war work have prevented the publication of a number of papers that otherwise would be available. Also, the subcommittees have made some progress with Standards, but it seems wise to postpone putting them into final form until the changes caused by the war have been evaluated. On the other hand, the war has stimulated developments in new instruments, particularly those required by aircraft, and some papers on these developments have been presented.

At the summer technical meeting in Cleveland, a single paper on the determination of the ratio and phase angle of potential transformers was presented. However, at the winter technical meeting, seven papers were presented, requiring a session and a half The papers in the morning session covered a wide scope, including two new methods of measurements by means of potentiometer circuits and vacuum-tube circuits, improvements in instrument construction and performance due to new permanent-magnet materials and the use of the new internal pivot construction, and methods of increasing the range and scope of power-factor indicators.

Three papers of considerable interest were presented in the afternoon session. One paper described a lumped R-C electric circuit used to study heat flow in thermal circuits. A second paper described a new servo system in which a secondary shaft is caused to follow accurately a primary shaft by means of polarized light whose plane of polarization rotates with the primary shaft. No load is imposed on the primary shaft. A third paper described the application of this servo system to a differential analyzer. Also, a paper of wide scope describing several methods of applying electronic circuits to measurements, "Electronic Devices Applied to Metallurgical Research," by E. V. Potter, is to be published in the general-interest section of *Electrical Engineering*.

At its meeting on November 19, 1943, the committee voted to circularize its members requesting suggestions as to means of improving the present method of grading papers. This was done, and the results have been submitted to the technical program committee.

A former subcommittee on telemetering was discharged with thanks in 1941 after it had completed its assignment of preparing the report, "Telemetering and Supervising Control." Because of recent advances in the art of telemetering and supervising control and its increasing importance, a new subcommittee, the membership of which consists of an equal number from the committee on instruments and measurements and the committee on automatic stations, has been appointed.

The committee on definitions has continued to study the *C*42 "Definitions of Electrical Terms," and has prepared a report in which is included a considerable number of suggested changes and additions. These have been circularized among the committee members and when approved will be submitted for the next revision of the *C*42 definitions.

The subcommittee on master test code for resistance measurements is attempting to prepare a code covering a limited but definite scope.

The newly appointed subcommittee on ultrahigh-frequency measurements finds it nearly impossible to obtain papers or to make reports because military secrecy is so strict. However, when the restrictions are removed, there is certain to be a large volume of interesting material.

Two members of the committee are filling important places in other engineering societies, H. M. Turner as president of the IRE for the year 1944, and Everett S. Lee as chairman of the ASME committee on industrial instruments and measurements. The ASME committee invited representatives of local societies interested in this field, including the members of our committee, to its meetings, and our committee reciprocated by inviting members of local industrial-control groups to attend our sessions at the winter technical meeting.

COMMITTEE ON PRODUCTION AND APPLICATION OF LIGHT

A conference on wartime influences on lighting was held under the sponsorship of this committee at the summer technical meeting in Cleveland, June 1943. At the Pacific Coast technical meeting held in Salt Lake City in September, a symposium on lighting, embracing four papers, was presented. Another conference on lighting, relating particularly to progress in light sources was held at the winter technical meeting in January 1944.

A meeting of the committee was held in October 1943, at which time it was agreed to continue the activities of the subcommittee on preferred characteristics of electric power for lighting.

Consideration has been given to the subject of recommending adoption of the proposed new international photometric units, and it has been agreed that action on this subject should be postponed until conditions are more propitious for making the proposed change than at present.

COMMITTEE ON MARINE TRANSPORTATION

The committee on marine transportation has been engaged actively during the year in reviewing and revising the war emergency Standard that was published last year. These revisions have resulted from receipt of more accurate service information and are intended to meet the changing demands for conservation of critical materials. This has developed into a major task, since it has been the committee's objective to standardize on substitute materials that are best fitted for the particular marine application. The immediate need, however, for substitution necessarily has prevented the extensive investigations, tests, and reports of service experience normally considered desirable prior to modification of the Standard. While experience may show that some of the substitutions are not of the quality exemplified by the basic Standard, it is believed that the reduction has been minor when compared with the immediate need of attaining an unprecedented output of ships. The extensive use of the committee's findings is best illustrated by the distribution of 3,000 copies of the war emergency recommendations between April 1, 1943, and January 1, 1944.

The committee's activities for the conservation of critical materials has been assisted greatly by a representative from the War Production Board who has placed at our disposal the results of the Board's multitudinous surveys of available material, production bottlenecks, and estimated near future requirements. The committee appreciates the excellent co-operation received from this representative.

The vast increase in ship construction, the greater use of electric equipment in connection with surface and air defense, and the increased use of untrained construction and installation personnel have required meticulous efforts on the part of the subcommittees in reviewing the wording of each section to avoid, as far as possible, misinterpretation by the many users. Investigations have been conducted relating to the construction, application, and methods of installation of electric equipment to withstand the shocks resulting from torpedo and bomb attacks, near misses, and so forth, as encountered on merchant-type vessels.

Many communications addressed to the Institute by ship designers, inspectors, installers, and others, concerning marine installation methods, materials and equipment have been acted on by the committee or its officers.

Within the last six months there has been a growing demand for incorporating in the basic Standard all the war emergency Standards that have proved satisfactory and will be advantageous for peacetime use. This has resulted from the Maritime Commission's program to construct "victory ships" which, as the name implies, are designed for long life and economic operation after the war. The subcommittees, therefore, are now engaged along with other activities in reviewing basic Standard 45 to accomplish this purpose.

COMMITTEE ON POWER GENERATION

The committee has two major projects, one of which will be completed this year. A joint subcommittee of this committee and ASME has been working actively on the preparation of a specification for prime-mover speed governors. The final report has been prepared and will be forwarded to the Standards committee for approval this year.

While the joint committee was appointed for the purpose of preparing a recommended specification covering the speed governing of all types of prime movers intended to drive electric generators, the specification for the present, is limited in its scope to steam-turbine generators of not less than 10,000 kw rated capacity.

During the year, an AIEE group was appointed to work in co-operation with ASME on the preparation of Standards for turbine generators in sizes of 10,000 to 60,000 kw. The committee on power generation is co-operating with the AIEE group on this project. In co-operation with the Standards committee, a conference was sponsored for a general discussion on the subject, and the committee assisted in the collection of operating data as a basis for the standardization. It also will sponsor a session at the summer technical meeting in St. Louis at which time an interim report will be presented.

COMMITTEE ON POWER TRANSMISSION AND DISTRIBUTION

Because of the difficulties of wartime travel, meetings of the committee have been kept to a minimum. A meeting of the subcommittee chairmen was held in October in New York, at which activities for the year were planned. A full meeting of the committee was held at the winter technical meeting in New York in January 1944, at which there was a large attendance, and many subjects of interest were discussed.

Technical Meetings. The committee sponsored two sessions at the summer technical meeting in Cleveland, one dealing with electric transients, and one with wartime emergency loading of conductors. This session continued work that was begun at the previous winter meeting on the same general subject. The committee also cooperated with other committees in a successful joint conference on resistance welding and its associated power-supply system. At the winter technical meeting in New York, the committee sponsored three sessions dealing with a variety of problems associated with transmission and distribution. For the summer meeting in St. Louis in June 1944, the committee is planning two or more sessions, one of which will deal with cable problems.

Subcommittee Activities. The distribution subcommittee has been at work on an analysis of the effect of expected postwar loads on the design of distribution systems.

The general systems subcommittee is reviewing the problems involved in the handling of kilovars on power systems. The subcommittee suggested that steps be taken to revise the definitions in standard C42, applying to the sign of kilovars, so that lagging kilovars should be regarded as positive. The main committee concurred in this suggestion, and the matter has been transmitted to the standards committee for consideration.

The stations subcommittee has been studying the problems connected with the increased use of air circuit breakers in station design. At the present time, it is circulating a questionnaire on automatic- and supervisory-control substations with a view of determining whether or not there is a need for greater use of this type of substation. The answer to this questionnaire may result in a conference session on the subject at the winter technical meeting in 1945.

The transmission subcommittee, through its working group on towers, poles, and conductors, sponsored a report on "Emergency Overloads on Overhead Conductors" at the summer meeting in Cleveland. The lightning and insulator group is preparing a further section to Standard 41, dealing with "Suggested Contractual Specifications for Insulators." The subcommittee is also preparing a report on "Lightning Resistant Transmission Lines." During the year, the nine conference papers presented at the winter meeting of 1943, sponsored by the cable working group, dealing with "Emergency Overloading of Insulated Power Cables," were issued in pamphlet form as special publication 6.

COMMITTEE ON PROTECTIVE DEVICES

Two meetings of the committee were held. The discussions dealt with the formulation of Standards, papers for technical sessions, subcommittee reports, and other means of advancing and improving the work of the committee and the interests of the Institute.

Papers sponsored by the committee included nine at the 1943 summer technical meeting, four at the 1943 Pacific Coast technical meeting, and 15 at the 1944 winter technical meeting.

Future studies will cover the effects of switching surges on insulation; apparatus and system performance; sensitive protection of distribution lines; and effects of wartime practices.

The committee has been active in the revision of existing and the preparation of new Standards. Work on Standard 19, "A-C Power Circuit Breakers," and Standard 31, "Capacitance Potential Devices and Outdoor Coupling Capacitors," was completed. Standards 20, "Low-Voltage Air Circuit Breakers"; 21, "Apparatus Bushings"; 22, "Air Switches and Bus Supports"; 25, "Fuses"; 32, "Neutral Grounding Devices"; and the proposed Standard 24 for "Distribution-Type Protector Tubes" are being actively carried forward, and it is expected they will soon be ready for final action and submission to the Standards committee.

The activities of the four subcommittees are summarized below.

Circuit Breakers, Switches, and Fuses. In addition to its work on Standards 19, 20, 21, 22, and 25, this subcommittee revised the short-cut method for calculating short-circuit currents, and the revision was published in *Electrical Engineering* for February 1944. A working group has prepared a first draft of a proposed Standard for fast-reclosing and short-circuiting devices for distribution lines. A second draft is expected soon.

Lightning Arresters. Standard 28, "Lightning Arresters," was approved by American Standards Association and has been designated C62. A working group is attempting to determine the characteristics of rod gaps as lightning-protective devices. The subcommittee prepared and presented, at the 1943 summer technical meeting, a paper on "The Use of Lightning Arresters to Help in the War Effort." It is studying surge and overvoltage protection of rotating machines.

Fault-Current-Limiting Devices. A draft of a revision of the proposed Standard, "Fault-Current-Limiting Devices," incorporating changes made as a result of suggestions, has been sent to the subcommittee members for further study.

A questionnaire on system-grounding practices is nearly ready to send out to a selected list for trial use. If it proves satisfactory, it will be sent to the entire committee membership.

Relays. Besides its work on Standard 31, the subcommittee arranged for a conference on the speed of relaying at the winter technical meeting, revised the relay bibliography, and made plans for further consideration of carrier-current auxiliaries, the application and economics of lower current-transformer secondary currents, Standards covering instrument transformers, and those phases of generator protection which are within the scope of the subcommittee.

Awards

COMMITTEE ON AWARD OF INSTITUTE PRIZES

Four national prizes and six District prizes were awarded for papers presented during the calendar year 1942 and for the student papers presented during the academic year ending June 30, 1943. The decrease from ten District prize awards reported in the year previous to only six District prize awards as reported herein is due to the effect of the training programs in the various colleges, which has resulted in fewer District and Branch paper prizes being awarded.

Several of the technical papers were of a high order, and in several of the classifications a choice between leading papers worthy of award was very close. The committee wishes to congratulate all authors for having made such substantial contributions to the profession. The committee also wishes to acknowledge the aid of the technical committees and their reviewers in nominating and grading papers, and in addition the aid of Mr. M. J. Kelly and Mr. B. W. Kendall.

EDISON MEDAL

The Edison Medal, awarded by a committee composed of 24 members of the Institute, was presented for 1943 to Vannevar Bush, president, Carnegie Institution of Washington, D. C., "for his contribution to the advancement of electrical engineering, particularly through the development of new applications of mathematics to engineering problems, and for his eminent service to the Nation in guiding the war research program." The presentation took place January 26, 1944, during the winter technical meeting. The medal may be awarded annually for "meritorious achievement in electrical science, electrical engineering, or the electrical arts."

LAMME MEDAL

The Lamme Medal committee awarded the medal for 1943 to Arthur H. Kehoe, vice-president, Consolidated Edison Company of New York, Inc., "for pioneer work in the development of a-c networks and associated apparatus for power distribution." Arrangements are being made for the presentation of the medal at the summer technical meeting in St. Louis, June 26–30, 1944. The medal may be awarded annually to a member of the AIEE "who has shown meritorious achievement in the development of electrical apparatus or machinery."

JOHN FRITZ MEDAL

The John Fritz Medal board of award, composed of representatives of the American Society of Civil Engineers, American Institute of Mining and Metallurgical Engineers, American Society of Mechanical Engineers, and AIEE, awarded the 39th medal for 1944 to Charles F. Kettering, vice-president in charge of research, General Motors Corporation, Dayton, Ohio, "for notable achievements in the field of industrial research which have contributed greatly to the welfare of mankind and of the nation."

HOOVER MEDAL

The Hoover Medal was established through a trust fund created by a gift from Conrad N. Lauer, and is to be awarded periodically "to a fellow engineer for distinguished public service" by a board representing the national societies of civil, mining and metallurgical, mechanical, and electrical engineers. The sixth medal, for 1942, was awarded to Gerard Swope, president, General Electric Company.

ALFRED NOBLE PRIZE

This prize, established in 1929, consists of a certificate and a cash award from the income of a fund contributed by engineers and others, to perpetuate the name and achievements of Alfred Noble, past president of the ASCE, and of the Western Society of Engineers. It may be made to a member of any of the co-operating societies, ASCE, AIME, ASME, AIEE, or WSE, for a technical paper of particular merit accepted by the publication committee of any of these societies, provided the author, at the time of such acceptance, is not over 30 years of age. The award for 1942–43 was presented to Benjamin J. Lazan, Sonntag Scientific Equipment Corporation, Greenwich, Conn., for his paper "Some Mechanical Properties of Plastics and Metals Under Sustained Vibrations."

Joint Activities

UNITED ENGINEERING TRUSTEES, INC.

This organization manages property and funds held jointly by the four founder societies, including the Engineering Societies Building, the Engineering Societies Library, and the endowment funds of the Engineering Foundation.

Increases in operating expenses and continuing decline in revenue from hall rentals produced an excess of expenses over income of $11,270.24, and the founder societies assumed responsibility for a large part of it.

The building is reported to be in excellent condition and is fully occupied by the founder societies and associates.

An abstract of the annual report of the corporation for the year which ended September 30, 1943, appeared in *Electrical Engineering* for January 1944, pages 36–7.

ENGINEERING FOUNDATION

The Engineering Foundation was established in 1914 as a joint organization of the four societies, ASCE, AIME, ASME, and AIEE, for "the furtherance of research in science and engineering, and the advancement in any other manner of the profession of engineering and the good of mankind."

Each research project supported by it is under the sponsorship of one of the founder societies.

One of the research projects sponsored by the Institute, insulating oils and cable saturants, at the Massachusetts Institute of Technology, was suspended as of August 31, 1943, with the thought that it may be desirable to resume the work after the war.

The comprehensive research program of the Welding Research Council is sponsored jointly by the Institute and the American Welding Society.

During its fiscal year which ended September 30, 1943, the foundation supported 14 projects. For the present year, provisions were made to continue six projects and to initiate one new project.

An extensive abstract of the annual report for 1942–43 appeared in *Electrical Engineering* for January 1944, pages 37–8.

ENGINEERING SOCIETIES LIBRARY

The Engineering Societies Library was formed by combining the separate libraries of the four national societies of civil, mining and metallurgical, mechanical, and electrical engineers, and preparing a composite card catalog.

Special services rendered by the library include: photoprints, searches, abstracts, translations, bibliographies, book loans by mail.

Table IX. Analysis of Employment Service

Month	Men Registered						Men Placed					
	New York	Chicago	San Francisco	Detroit	Boston	Total	New York	Chicago	San Francisco	Detroit	Boston	Total
1943												
May	109	28	54	15	17	223	24	6	15	3	4	52
June	103	22	81	10	22	238	32	7	18	2	1	60
July	102	48	60	22	23	255	30	15	22	11	5	83
August	110	35	59	30	24	258	32	10	21	8	7	78
September	127	25	53	20	27	252	30	12	21	13	5	81
October	81	32	49	17	15	194	34	8	29	3	14	88
November	105	17	64	20	18	224	28	5	16	8	10	67
December	88	20	43	12	16	179	20	13	11	6	3	53
1944												
January	105	20	49	15	31	220	31	8	11	3	7	50
February	92	16	57	17	21	203	24	6	23	4	5	62
March	112	23	57	7	28	227	22	9	19	3	7	60
April	83	23	47	17	15	185	25	8	23	4	10	70
Total	1,217	309	673	202	257	2,658	332	107	229	68	78	804

The library has been used extensively by the Army and the Navy, and has supplied much information to members and others engaged in war work, such as details of manufacturing processes and foreign docks, factories, and other installations of military significance.

An abstract of the annual report of the library appeared on pages 38–9 of *Electrical Engineering*, January 1944.

EMPLOYMENT SERVICE

Operating as an incorporated nonprofit organization, the Engineering Societies Personnel Service, Inc., assists members of the Founder Societies desiring to secure new positions and employers searching for engineers. Nonmembers may receive such assistance when positions available cannot be filled by members.

Offices are operated in New York, N. Y.; Boston, Mass.; Detroit, Mich.; Chicago, Ill.; and San Francisco, Calif.; with the co-operation of the Engineering Societies of New England in Boston, the Engineering Society of Detroit in that city, the Western Society of Engineers in Chicago, and the Engineers Club of San Francisco in that city.

The service is supported by the joint contributions of the societies and the individuals who secure positions. An analysis of registration and placement records as reported to the national societies is given in Table IX.

ENGINEERS' COUNCIL FOR PROFESSIONAL DEVELOPMENT

This council, organized in 1932 to engage in activities leading toward the enhancement of the professional status of the engineer, includes three representatives of each of the eight participating organizations, which are the national societies of civil, electrical, mechanical, and mining and metallurgical engineers, the Society for the Promotion of Engineering Education, the National Council of State Boards of Engineering Examiners, and the Engineering Institute of Canada. Its principal activities have been carried on by four committees: student selection and guidance, engineering schools, professional training, and professional recognition.

Despite war conditions progress in the activities has continued, and attention has been given to problems arising from the war.

One of the most important accomplishments was the development and adoption of a plan for establishing closer co-operation with local groups of engineers.

A comprehensive report on the annual meeting, including the reports of committees, was published in *Electrical Engineering* for December 1943, pages 553–7.

JOINT CONFERENCE COMMITTEE

This committee, composed of the presidents, immediate past presidents, and secretaries of the Founder Societies and the American Institute of Chemical Engineers, considered numerous topics of interest to the societies. It recommended that each of the societies appoint three representatives to serve on a joint committee to study and make recommendations regarding the most desirable form of organization of the engineering profession.

RADIO TECHNICAL PLANNING BOARD

This joint advisory body will formulate recommendations on the technical future of radio developments for submission to the Government, industry, and the public.

A general plan of organization was approved September 15, 1943, and details of panel organization were adopted September 29.

Initial member bodies in addition to the AIEE are: Radio Manufacturers Association; Institute of Radio Engineers; American Institute of Physics; American Radio Relay League; F M Broadcasters, Inc.; International Association of Chiefs of Police; National Association of Broadcasters; and National Independent Broadcasters. The board includes one representative of each member organization.

Thirteen technical panels have been organized, covering the fields of spectrum utilization; frequency allocation; high-frequency generation; standard broadcasting; very high-frequency broadcasting; television; facsimile; radio communication; relay system; radio range, direction, recognition; aeronautical radio; industrial, scientific, and medical equipment; and police emergency services.

REPRESENTATIVES

In addition to representation in the joint organizations previously mentioned, the Institute is represented in numerous other joint activities of interest to engineers in general. The names of its representatives in more than 30 joint committees and national organizations were published in the September 1943 issue of *Electrical Engineering*, pages 420–1, and in the 1944 *Year Book*.

APPRECIATION

The continuing rapid increase in the membership and the income of the Institute are tangible symbols of the highly effective work of the national committees, and the District, Section, and Branch officers and committees. For their enthusiasm and generous contributions of time under adverse conditions, the board of directors expresses sincere thanks and appreciation. It also thanks the members in general for their keen interest in the activities and for their splendid support of established policies.

Respectfully submitted for the board of directors.

H. H. HENLINE
National Secretary

May 25, 1944

HASKINS & SELLS
CERTIFIED PUBLIC ACCOUNTANTS

1 EAST 44TH STREET
NEW YORK

May 18, 1944

American Institute of Electrical Engineers,
33 West 39th Street, New York.

Dear Sirs:

We have examined the balance sheet of the American Institute of Electrical Engineers, and schedule of securities owned, as of April 30, 1944, and the related statements of cash receipts and disbursements of operating and restricted funds for the year ended that date, have reviewed the accounting procedures of the Institute, and have examined its accounting records and other evidence in support of such financial statements. Our examination was made in accordance with generally accepted auditing standards applicable in the circumstances and included all auditing procedures we considered necessary, which procedures were applied by tests to the extent we deemed appropriate in view of the system of internal control.

In our opinion, the accompanying balance sheet, schedule of securities owned, and statements of cash receipts and disbursements fairly present, respectively, the financial condition of, and securities owned by, the Institute as of April 30, 1944, and its recorded cash receipts and cash disbursements for the year ended that date, in conformity with generally accepted accounting principles and practices applied on a basis consistent with that of the preceding year.

Yours truly,

(*Signed*) HASKINS & SELLS

AMERICAN INSTITUTE OF ELECTRICAL ENGINEERS

(Incorporated in New York)

Balance Sheet, April 30, 1944

Exhibit A

ASSETS		
Property Fund Assets:		
One-fourth interest in physical properties of United Engineering Trustees, Inc.:		
Land, buildings, and equipment (less depreciation and renewal reserve)	$379,812.45	
Funded depreciation and renewal reserve	118,636.03	
Total	$498.448.48	
Equipment:		
Library—volumes and fixtures—estimated value	36,366.37	
Office furniture and fixtures (less reserve for depreciation, $27,679.83)	7,528.52	
Works of art, etc.	3,001.35	
Investment, less reserve, $415.73—Schedule 1	528.00	
Total property fund assets		$545,872.72
Restricted Fund Assets:		
Securities—at cost, less reserve, $13,088.92 (quoted market value, $323,441.37)—Schedule 1	$315,225.29	
Cash (Exhibit C)	32,009.57	
Accrued interest receivable	116.42	
Total restricted fund assets		347,351.28
Operating Fund Assets:		
Cash (not including $1,079.42 for Federal taxes withheld from employees) (Exhibit B)	$ 53,749.13	
Accounts receivable:		
Members—for dues (less reserve, $8,000.00)	8,619.68	
Advertisers	3,255.40	
Miscellaneous	2,225.42	
Accrued interest receivable	4,159.60	
Inventories:		
Transactions, etc.	993.25	
Electrical Definitions	5,391.95	
Work in process (May issue of *Electrical Engineering*, etc.)	4,611.89	
Text and cover paper	7,787.10	
Badges	1,554.62	
Total operating fund assets		92,348.04
Total		**$985,572.04**

LIABILITIES		
Property Fund Reserve		$545,872.72
Restricted Fund Reserves:		
Reserve Capital fund	$328,329.58	
Life Membership fund	7,873.98	
International Electrical Congress of St. Louis Library fund	5,618.05	
Lamme Medal fund	4,425.33	
Mailloux fund	1,104.34	
Total restricted fund reserves		347,351.28
Operating Fund Reserves, Liabilities, Etc.:		
Accounts payable	$ 12,763.58	
Deferred income:		
Dues received in advance	5,427.45	
Entrance fees and dues advanced by applicants for memberships	835.65	
Subscriptions to publications received in advance	9,621.86	
Miscellaneous (including unallocated receipts)	808.91	
Operating fund reserves	62,890.59	
Total operating fund reserves, liabilities, etc.		92,348.04
Total		**$985,572.04**

AMERICAN INSTITUTE OF ELECTRICAL ENGINEERS

Statement of Cash Receipts and Disbursements of Operating Fund for the Year Ended April 30, 1944

Exhibit B

Cash on Deposit With The National City Bank of New York, May 1, 1943		$ 54,204.19
Receipts:		
Dues (including $104,376.00 allocated to *Electrical Engineering* subscriptions)	$230,060.90	
Advertising	105,077.44	
Transactions subscriptions	8,169.19	
Electrical Engineering subscriptions	17,494.55	
Electrical Definitions	1,532.77	
Miscellaneous publications (preprints, Standards, *Transactions* index, etc.)	20,848.34	
Students' fees	10,787.90	
Entrance fees	14,835.25	
Membership badges	4,277.95	
Transfer fees	2,691.65	
Interest and dividends on investments of Restricted Capital fund	10,283.26	
Transfer from:		
Reserve Capital fund	9,500.00	
Life Membership fund	274.25	
Miscellaneous	181.27	
Total receipts		436,014.72
Total		$490,218.91
Disbursements:		
Publication expense:		
Electrical Engineering	$100,683.87	
Transactions	8,527.54	
Year Book	7,082.38	
Miscellaneous publications (preprints, Standards, *Transactions* supplement, etc.)	16,210.07	
Institute meetings	14,237.80	
Institute Sections	38,836.19	
Institute Branches	2,649.05	
Finance committee	712.00	
Headquarters committee	58.24	
Membership committee	10,492.80	
Standards committee	9,517.93	
Pension fund committee	642.47	
Technical committees	584.72	
Forward	$210,235.06	$490,218.91
Total (forward)		$490,218.91
Disbursements (forward)	$210,235.06	
Retirement system—AIEE initial payment (see Exhibit C for payment of $10,000.00 from Pension Fund)	75,000.00	
Retirement system—AIEE normal contribution	2,262.54	
Radio Technical Planning Board	1,000.00	
Traveling expenses:		
Geographical Districts:		
Executive committees	5,462.10	
Vice-presidents	1,001.21	
Branch counselors and chairmen	4,199.13	
President's appropriation	1,300,97	
Board of directors	5,413.29	
National nominating committee	1,141.11	
Administrative expenses	52,617.15	
Geographical Districts—Branch paper prizes	168.00	
Institute prizes, national	409.00	
American Co-ordinating Committee on Corrosion	25.00	
American Standards Association	1,500.00	
United Engineering Trustees, Inc.:		
Building assessment	17,468.21	
Library assessment	10,679.35	
Engineers' Council for Professional Development	1,700.00	
Engineering Foundation Project:		
Welding research	250.00	
International Committee on Illumination	100.00	
John Fritz medal	50.00	
National Fire Protection Association—Dues	60.00	
Membership badges	3,460.62	
Legal services	250.00	
Transfer to Reserve Capital fund	40,000.00	
Office furniture and fixtures, and repairs	280.52	
Miscellaneous	436.52	
Total disbursements		$436,469.78
Cash on Deposit With The National City Bank of New York, April 30, 1944 (not including $1,079.42 for Federal taxes withheld from employees)		$ 53,749.13

AMERICAN INSTITUTE OF ELECTRICAL ENGINEERS

Statement of Cash Receipts and Disbursements of Restricted Funds for the Year Ended April 30, 1944

Exhibit C

	Restricted Funds						
	Reserve Capital Fund	Pension Fund	Life Membership Fund	International Electrical Congress of St. Louis Library Fund	Lamme Medal Fund	Mailloux Fund	Total Restricted Funds
Cash on Deposit With The National City Bank of New York and Various Savings Banks, May 1, 1943	$ 35,287.71	$10,000.00	$2,715.05	$271.80	$ 27.83	$1,087.97	$ 49,390.36
Receipts:							
Interest on bonds			$129.78	$130.00	$262.04		$521.82
Interest on bank balances			39.37			16.37	55.74
Liquidating dividends—International Match Realization Co., Ltd.	$219.00						219.00
Proceeds from sale of securities	29,945.04				1,747.96		31,693.00
Transfer from operating fund	40,000.00						40,000.00
Transfer from reserve capital fund					2,690.00		2,690.00
Life membership fee			217.19				217.19
Total receipts	$ 70,164.04		$ 386.34	$130.00	$4,700.00	$ 16.37	$ 75,396.75
Total	$105,451.75	$10,000.00	$3,101.39	$401.80	$4,727.83	$1,104.34	$124,787.11
Disbursements:							
Purchase of securities	$ 65.585.46				$4,372.00		$ 69,957.46
Retirement system—AIEE initial payment (see Exhibit B for payment of $75,000.00 from operating fund)		$10,000.00					10,000.00
Transfers to:							
Operating fund	9,500.00		$ 274.25				9,774.25
Lamme fund	2,690.00						2,690.00
Lamme gold medals, replicas, and engrossing certificates					355.83		355.83
Total disbursements	$ 77,775.46	$10,000.00	$ 274.25		$4,727.83		$ 92,777.54
Balance on Deposit With The National City Bank of New York and Various Savings Banks, April 30, 1944	$ 27,676.29		$2,827.14	$401.80		$1,104.34	$ 32,009.57

AMERICAN INSTITUTE OF ELECTRICAL ENGINEERS

Securities Owned, April 30, 1944

Schedule 1

	Principal Amount of Bonds or Number of Shares of Stock	Restricted Funds					Property Fund (Equipment Replacements)
		Reserve Capital Fund	Life Membership Fund	International Electrical Congress Library Fund	Lamme Medal Fund	Total Restricted Fund	
Railroad Bonds:							
Baltimore & Ohio, Pittsburgh, Lake Erie & West Virginia System 4%, due 1951	$10,000.00	$ 6,450.00				$ 6,450.00	
Chicago & Erie Railroad Company 5% first mortgage, due 1982	1,000.00	1,105.00				1,105.00	
New York Central Railroad Company 4% series A consolidated mortgage, due 1998	10,000.00	6,100.00				6,100.00	
Oregon-Washington Railroad & Navigation Company 4% series A, due 1961	4,000.00				$4,372.00	4,372.00	
St. Louis-San Francisco Railway Company 5% prior lien mortgage series B, due 1950 (certificates of deposit)	6,000.00*	5,497.50*				5,497.50*	
Total railroad bonds		$ 19,152.50			$4,372.00	$ 23,524.50	
Public Utility Bonds:							
New York & Queens Electric Light & Power Company 3½% first and consolidated mortgage, due 1965	10,000.00	$ 11,000.00				$ 11,000.00	
Bond and Real Estate Mortgage:							
Fidelity Union Title and Mortgage Guaranty Company first mortgage certificates (on property 75–79 Prospect Street, East Orange, N. J.) 4%, due 1944	14,132.00†	$ 13,188.27†				$ 13,188.27†	$943.73†
United States Government Bonds:							
Treasury Savings bonds series D, due July 1, 1949	10,000.00	$ 7,644.00				$ 7,644.00	
Treasury Savings bonds series D, due January 1, 1950	10,000.00	7,500.00				7,500.00	
Treasury bonds 2%, due 1950/48	77,000.00	78,130.94				78,130.94	
Treasury bonds 2½%, due 1968/63	20,000.00	15,000.00	$5,000.00			20,000.00	
Treasury bonds 2½%, due 1972/67	6,300.00	1,100.00		$5,200.00		6,300.00	
Defense bonds series F, due June 1, 1953	34,000.00	25,160.00				25,160.00	
Defense bonds series G, 2½%, due December 1, 1954	18,000.00	18,000.00				18,000.00	
War Savings Bonds series G, 2½%, due September 1, 1955	40,000.00	40,000.00				40,000.00	
Total United States Government bonds		$192,534.94	$5,000.00	$5,200.00		$202,734.94	
Capital Stocks:							
American Can Company	60 shares	$ 4,988.40				$ 4,988.40	
American Telephone & Telegraph Company	30 shares	4,897.95				4,897.95	
Atchison, Topeka & Sante Fe Ry. Company—Preferred	150 shares	13,035.46				13,035.46	
Boston Edison Company	200 shares	4,927.50				4,927.50	
Commonwealth Edison Company	200 shares	7,580.68				7,580.68	
Consolidated Natural Gas Company	11 shares	221.92				221.92	
Eastman Kodak Company	35 shares	4,768.23				4,768.23	
E. I. du Pont de Nemours & Company	40 shares	6,398.20				6,398.20	
General Electric Company	130 shares	4,463.80				4,463.80	
General Motors Corporation	100 shares	4,235.53				4,235.53	
International Harvester Company	100 shares	5,030.50				5,030.50	
International Match Realization Co., Ltd. voting trust certificates for capital shares of International Match Corporation	6 shares*	1,875.15*				1,875.15*	
Sears Roebuck & Company	100 shares	6,014.97				6,014.97	
Standard Oil Company of New Jersey	110 shares	4,569.86				4,569.86	
Union Carbide & Carbon Corporation	70 shares	4,858.35				4,858.35	
Total capital stocks		$ 77,866.50				$ 77,866 50	
Total		$313,742.21	$5,000.00	$5,200.00	$4,372.00	$328,314.21	$943.73
Less reserves for securities considered to be of doubtful value:							
*Reserve in full for securities designated *		$ 7,372.65				$ 7,372.65	
†Reserve of approximately 40% for securities designated †		5,716.27				5,716.27	$415.73
Total		$ 13,088.92				$ 13,088.92	$415.73
Total Securities, Less Reserve		$300,653.29	$5,000.00	$5,200.00	$4,372.00	$315,225.29	$528.00

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Sections of the Institute

Name	District	When Organized	Membership Aug. 1, 1944	Chairman	Secretary	Secretary's Address
Akron	2	Aug. 12, '20	104	A. G. Seifried	P. S. Bechtol	1359 Ottawa Ave., Akron 5, Ohio
Alabama	4	May 22, '29	44	F. G. Hamner	T. E. Curtis	Southern States Equipment Corp., 12 S. 20th St ., Birmingham 3, Ala.
Arizona	8	Mar. 22, '41	50	C. R. Smith	E. A. Gissel	Central Arizona Light & Power Co., Phoenix, Ariz.
Boston	1	Feb. 13, '03	604	A. L. O'Banion	R. E. Muehlig	Westinghouse Elec. & Mfg. Co., 235 Old Colony Ave., Boston, Mass.
Central Indiana	5	Jan. 12, '12	148	C. E. Parks	S. C. Leibing	General Electric Co., Traction Terminal Bldg., Indianapolis, Ind.
Chicago	5	1893	869	R. C. Ericson	J. F. Calvert	Northwestern Technological Institute, Evanston, Ill.
Cincinnati	2	June 30, '20	149	A. J. Allen	G. W. Coulehan	607 Chamber of Commerce Bldg., Cincinnati 2, Ohio
Cleveland	2	Sept. 27, '07	390	W. R. Hough	J. D. Leitch	The Electric Controller & Mfg. Co., 2700 E. 79th St., Cleveland, Ohio
Columbus	2	Mar. 17, '22	84	R. W. Miner	N. J. Greene	National Electric Coil Co., Columbus 8, Ohio
Connecticut	1	Apr. 16, '21	363	Carl F. Scott	C. D. Hewitt	P. O. Box 1562, New Haven 6, Conn.
Dayton	2	June 9, '43	173	J. W. Gehrke	F. S. Himebrook	Master Electric Co., Dayton, Ohio
Denver	6	May 18, '15	192	Hubert Sharp	L. R. Patterson	Public Service Co. of Colorado, Denver, Colo.
East Tennessee	4	Sept. 2, '36	206	J. D. Harper	F. A. Hoeke	General Electric Co., 602 S. Gay St., Knoxville, Tenn.
Erie	2	Jan. 11, '18	74	J. Hause	W. C. Brown	General Electric Co., Erie, Pa.
Florida	4	Jan. 28, '31	134	C. F. Titus	P. J. Carlin	Florida Power & Light Co., Box 3100, Miami 30, Fla.
Fort Wayne	5	Aug. 14, '08	107	W. W. Brooks	S. A. Zimmermann	2600 Hoagland Ave., Fort Wayne, Ind.
Georgia	4	Jan. 14, '04	115	T. J. Woth	R. O. Loomis	Georgia Power Co., Atlanta 1, Ga.
Houston	7	Aug. 7, '28	180	Hezzie Clark	R. S. Kersh	612 Petroleum Bldg., Houston 2, Texas
Iowa	5	June 29, '29	78	E. B. Fowler	J. A. Green	Collins Radio Co., 855—35th St., N. E., Cedar Rapids, Iowa
Ithaca	1	Oct. 15, '02	65	E. M. Strong	H. G. Smith	Cornell University, School of Elec. Engg., Ithaca, N. Y.
Kansas City	7	Apr. 14, '16	146	M. E. Bretschneider	P. C. Cross	General Electric Co., 106 W. 14th St., Kansas City 6, Mo.
Lehigh Valley	2	Apr. 16, '21	179	H. E. Pearson	J. A. G. Oewel	Penn. Pwr. & Lt. Co., 901 Hamilton St., Allentown, Pa.
Los Angeles	8	May 19, '08	635	P. L. Johnson	E. W. Morris	Westinghouse Elec. & Mfg. Co., 420 So. San Pedro St., Los Angeles 13, Calif.
Louisville	4	Oct. 15, '26	75	M. S. Winstandley	S. H. Gates	So. Bell Tel. & Tel. Co., 521 W. Chestnut St., Louisville 2, Ky.
Lynn	1	Aug. 22, '11	211	M. S. Wilson	R. G. Connors	788 Broad St., Lynn, Mass.
Madison	5	Jan. 8, '09	75	F. D. Mackie	G. F. Tracy	Elec. Lab., University of Wisconsin, Madison 5, Wis.
Mansfield	2	Mar. 6, '39	65	J. M. Robinson	H. G. Duhamel	North Electric Mfg. Co., Galion, Ohio
Maryland	2	Dec. 16, '04	381	B. Van Ness, Jr.	H. H. Angel	Bethlehem Steel Co., Sparrows Point, Md.
Memphis	4	May 22, '30	72	T. H. Allen	J. A. Crisman	Westinghouse Elec. & Mfg. Co., 825 Exchange Bldg., Memphis 1, Tenn.
Mexico	3	June 29, '22	83	A. Paez	E. F. Lopez	Apartado 13-Bis, Mexico, D. F. Mexico
Michigan	5	Jan. 13, '11	404	M. M. Cory	L. W. Clark	Detroit Edison Co., 2000—2nd Ave., Detroit 26, Mich.
Milwaukee	5	Feb. 11, '10	371	T. B. Jochem	J. W. Pomazal	Louis Allis Co., 427 E. Stewart St., Milwaukee 4, Wis.
Minnesota	5	Apr. 7, '02	124	C. W. Lethert	F. W. Hotchkiss	Electric Machine Mfg. Co., Minneapolis, Minn.
Montana	9	June 24, '31	36	I. J. Buergey	W. R. Neumann	Montana Power Company, Great Falls, Mont.
Montreal	10	Apr. 16, '43	208	F. L. Lawton	J. R. Auld	Canadian Industries Ltd., Engg. Dept., Montreal, P. Q., Canada
Muscle Shoals	4	Feb. 18, '38	22	W. E. Lindemann	E. L. Bishop	76 F St. Village 1, Wilson Dam, Ala.
Nebraska	6	Jan. 21, '25	51	M. L. Burgess	I. M. Ellestad	Northwestern Bell Telephone Co., Omaha, Nebr.
New Mexico-West Texas	7	Mar. 7, '40	54	R. C. Dickson	J. F. Armstrong	3219 Altura Blvd., El Paso, Tex.
New Orleans	4	Dec. 8, '33	151	B. H. Bell	L. J. Olivier	Louisiana Pwr. & Lt. Co., New Orleans 14, La.
New York	3	Dec. 10, '19	3,814	M. D. Hooven	W. S. Hill	General Electric Co., 570 Lexington Ave., New York 22, N. Y.
Niagara Frontier	1	Feb. 10, '25	185	W. K. Parks	W. J. Freudenberger	Buffalo Niagara Elec. Corp., 300 Electric Bldg., Buffalo 3, N. Y.
North Carolina	4	Mar. 21, '29	119	H. B. Robinson	L. M. Keever	North Carolina State College, Raleigh, N. C.
North Texas	7	May 18, '28	177	E. H. Flath	R. W. Roessler	1007 Southland Annex, Dallas, Tex.
Oklahoma City	7	Feb. 16, '22	100	C. L. Farrar	C. L. Jobe	Oklahoma Gas & Electric Co., Oklahoma City 2, Okla.
Philadelphia	2	Feb. 18, '03	892	A. C. Muir	M. L. Lehman	American Tel. & Tel. Co., 520 Bourse Bldg., Philadelphia 6, Pa.
Pittsburgh	2	Oct. 13, '02	663	R. C. Gorham	C. M. Skooglund	West Penn Power Co., 14 Wood St., Pittsburgh 30, Pa.
Pittsfield	1	Mar. 25, '04	196	J. L. Cantwell	T. E. Rodhouse	General Electric Co., 100 Woodlawn Ave., Pittsfield, Mass.
Portland	9	May 18, '09	189	W. E. Enns	O. A. Demuth	Box 3537, Portland 12, Oreg.
Providence	1	Mar. 12, '20	101	R. E. Secord	G. E. Andrews	The Narragansett Elec. Co., Melrose St., Providence 1, R. I.
Rochester	1	Oct. 9, '14	124	O. L. Angevine, Jr.	E. B. Kempster	General Motors Corp., Rochester 3, N. Y.
St. Louis	7	Jan. 14, '03	320	C. M. Lovell	Ray Robson	Union Electric Co. of Missouri, Webster Groves, Mo.
San Diego	8	Jan. 18, '39	74	W. J. Kenline	C. F. McCabe	Consolidated Aircraft Corp., Lindbergh Field, San Diego, Cailf.
San Francisco	8	Dec. 23, '04	682	G. C. Tenney	A. Tilles	Dept. of Elec. Engg., Univ. of California, Berkeley, Calif.
Schenectady	1	Jan. 26, '03	619	L. A. Umansky	L. T. Rader	General Electric Co., Industrial Control Engg. Dept., Schenectady, N. Y.
Seattle	9	Jan. 19, '04	274	C. H. Cutter	H. R. Brown	2136—47th Ave., S. W., Seattle 6, Wash.
Sharon	2	Dec. 11, '25	135	F. D. Fielder	A. D. Forbes	Westinghouse Elec. & Mfg. Co., Sharon, Pa.
South Bend	5	Feb. 26, '41	60	C. M. Dunn	C. D. Highleyman	Indiana & Michigan Elec. Co., So. Bend, Ind.
South Carolina	4	Mar. 2, '40	47	F. T. Tingley	J. L. Weeks	South Carolina Elec. & Gas Co., Columbia, S. C.
South Texas	7	May 23, '30	48	I. A. Uhr	O. T. Lodal	1411 Transit Tower, San Antonio, Tex.
Spokane	9	Feb. 14, '13	103	R. C. Kelly	W. E. Mathews	W. 208 Euclid, Spokane 13, Wash.
Springfield	1	June 29, '22	55	L. P. Kongsted	W. S. Scheering	Western Massachusetts Elec. Co., 73 State St., Springfield 2, Mass.
Syracuse	1	Aug. 12, '20	126	J. W. Dice	C. E. von Sothen	General Electric Co., 113 S. Salina St., Syracuse, N. Y.
Toledo	2	June 3, '07	87	R. L. Manor	W. M. Campbell	2145 Central Grove Ave., Toledo, Ohio
Toronto	10	Sept. 30, '03	381	A. W. Murdock	J. F. Moore	Dunlop Wire & Rubber Goods Ltd., 870 Queen St. E., Toronto, Canada
Tulsa	7	Oct. 1, '37	73	H. A. Norberg	E. F. Patterson	414 National Bank of Tulsa Bldg., Tulsa 3, Okla.
Urbana	5	Nov. 25, '02	87	E. A. Reid	P. F. Schwarzlose	212 Elec. Engg. Lab., University of Illinois, Urbana, Ill.
Utah	9	Mar. 9, '17	79	E. Bachman	J. A. McDonald	General Elec. Co., P. O. Box 779, Salt Lake City 9, Utah
Vancouver	10	Aug. 22, '11	101	L. B. Stacey	F. H. Hedley	1418 Marine Bldg., Vancouver, B. C., Canada
Virginia	4	May 19, '22	147	A. F. Forbes	J. A. Rawls	Virginia Elec. & Pwr. Co., Richmond, Va.
Washington	2	Apr. 9, '03	707	W. F. Dietz	H. M. Bollinger	Chesapeake & Potomac Tel. Co., 1111 N. Capitol St., Washington 2, D. C.
West Virginia	2	Apr. 9, '40	39	L. W. Roush	E. B. Ellerbe	Box 911, Charleston 23, W. Va.
Wichita	7	Sept. 16, '37	74	E. H. Stephens	H. E. Hartman	Kansas Gas & Elec. Co., 201 N. Market St., Wichita 1, Kansas
Worcester	1	Feb. 18, '20	65	V. F. P. Sepavich	R. M. Peirce	American Steel & Wire Co., Electrical Cable Works, Worcester, Mass.
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Clarkson College of Tech., Potsdam, N. Y.	1	A. R. Powers	David Wright	Donald Baker
Clemson Agri. College, Clemson, S. C.	4	T. F. Tingley	J. C. Martin	H. N. Koolage, Jr.
Colorado State Col. of A. & M. Arts, Fort Collins, Colo.	6		Glen Powers	Ralph Green
Colorado, Univ. of, Boulder, Colo.	6	H. B. Palmer	G. D. Doty	R. J. McCoy
Columbia Univ., New York, N. Y.	3	W. A. LaPierre	A. W. Huseby	J. Van Zweden
Connecticut, Univ. of, Storrs, Conn.	1	P. H. Nelson	Fred Winn	A. E. Anderson
Cooper Union, New York, N. Y.	3	E. E. Shelton		
(Day Division)			Norman Austern	G. E. Dombrowski
(Evening Division)			J. J. Coughlin	S. Meyers
Cornell Univ., Ithaca, N. Y.	1	E. T. B. Gross	R. O. Sinclair	C. F. DeSieno
Delaware, Univ. of Newark, Del.	2	M. G. Young	Wm. Schuster	
Denver, Univ. of, Denver, Colo.	6	F. H. McClain	Maynard Thompson	Walter Magnuson
Detroit, Univ. of, Detroit, Mich.	5	H. O. Warner	Thomas Stewart	J. Battocletti
Drexel Inst. of Tech., Philadelphia, Pa.	2	E. O. Lange	Paul Beroza	D. E. Thomas
Duke Univ., Durham, N. C.	4	O. J. Meier, Jr.	W. R. McMurran	R. M. Dunaiski
Florida, Univ. of, Gainesville, Fla.	4	E. F. Smith	Leslie Stroup	O. R. Gano
George Washington Univ., Washington, D. C.	2	M. K. Akers	A. H. Barauck	George Conrad
Georgia School of Tech., Atlanta, Ga.	4	H. B. Duling	R. E. Morris	L. S. Apple
Harvard Univ., Cambridge, Mass.	1	J. D. Cobine	J. C. Fisher	
Idaho, Univ. of, Moscow, Idaho	9	J. H. Johnson	H. Kambitsch	Gerald Hattrup
Illinois Inst. of Tech., Chicago, Ill.	5	T. J. Higgins	R. M. G. Sodaro	Wm. Kneen
Illinois, Univ. of, Urbana, Ill.	5	E. A. Reid	Robert Nadeau	Fred Grisak
Iowa State College, Ames, Iowa	5	B. S. Willis	James Waxweiler	R. R. Ragan
Iowa, Univ. of, Iowa City, Iowa	5		L. W. Quirk	H. W. Barnes
Johns Hopkins Univ., Baltimore, Md.	2	M. W. Pullen	G. M. Miller	D. L. Birx
Kansas State College, Manhattan, Kan.	7	R. G. Kloeffler	E. G. May	James Glenn
Kansas, Univ. of, Lawrence, Kan.	7	G. A. Richardson	Bob Stevenson	R. K. Johnson
Kentucky, Univ. of, Lexington, Ky.	4	Brinkley Barnett	R. L. Hucaby	
Lafayette College, Easton, Pa.	2	Morland King	Robert Brauburger	Norman Edwards
Lehigh Univ., Bethlehem, Pa.	2	C. G. Brennecke	Donald Hamme	Charles Downes
Louisiana State Univ., Baton Rouge, La.	4		L. E. Gulledge	P. W. Shefler
Louisville, Univ. of, Louisville, Ky.	4	S. T. Fife	Gene Barber	J. L. Wedekind
Maine, Univ. of, Orono, Me.	1	W. J. Creamer	D. E. Davis	
Manhattan College, New York, N. Y.	3	R. T. Weil	R. E. Blewitt	D. G. O'Connor
Marquette Univ., Milwaukee, Wis.	5	J. F. H. Douglas	Leon Kirchmayer	G. F. Risley
Maryland, Univ. of, College Park, Md.	2	L. J. Hodgins	W. E. Scull	A. H. Ballard
Mass. Inst. of Tech., Cambridge, Mass.	1	K. L. Wildes	George Lawrence	Robert Fauvre
Michigan Col. of Min. & Tech., Houghton, Mich.	5	G. W. Swenson	J. D. Dawson	P. Belopavlovich
Michigan State Col., E. Lansing, Mich.	5	M. M. Cory	John Allen	John McKeehan
Michigan, Univ. of, Ann Arbor, Mich.	5	J. S. Gault	W. B. Middleton	E. A. Hanyss
Milwaukee Sch. of Engg., Milwaukee, Wis.	5	E. L. Wiedner	E. M. Sieron	G. E. Skorup
Minnesota, Univ. of, Minneapolis, Minn.	5	J. H. Kuhlmann	J. G. Anderson	V. W. Beck
Mississippi State Col., State College, Miss.	4	L. L. Patterson		J. E. King, Jr.
Missouri Sch. of Mines & Met., Rolla, Mo.	7	F. H. Frame	E. Chestnut	R. Paulter
Missouri, Univ. of, Columbia, Mo.	7	M. P. Weinbach	Robert Roney	Koho Ozone
Montana State College, Bozeman, Mont.	9	E. W. Schilling	Howard Ellsworth	Rod Auclair
Nebraska, Univ. of, Lincoln, Neb.	6	O. E. Edison	J. R. Wallin	F. K. Ishii
Nevada, Univ. of, Reno, Nevada	8	S. G. Palmer	Wm. Richter	J. R. Baker
Newark Col. of Engg., Newark, N. J.	3	J. C. Peet	Robert Sugarman	Fred Bender
New Hampshire, Univ. of, Durham, N. H.	1	W. B. Nulsen	Richard Foley	A. G. Yeaton
New Mexico State Col., State College, N. M.	7	M. A. Thomas	Jack Burleson	Paul Russell
New Mexico, Univ. of, Albuquerque, N. M.	7	R. W. Tapy	A. R. Lloyd	S. H. Gold
New York, Col. of the City of, New York	3	Harry Baum		
(Day Division)			Solomon Feldman	Marvin Sleven
(Evening Division)				
New York Univ., New York	3	P. C. Cromwell		
(Day Division)			A. H. Morgenstein	G. A. Karron
(Evening Division)			Donald MacInnes	Andrew Painter
North Carolina State Col., Raleigh, N. C.	4	L. M. Keever	J. M. Hollander	J. S. Hunter
North Dakota State Col., Fargo, N. D.	5	H. S. Rush		
North Dakota, Univ. of, Grand Forks, N. D.	5	C. W. Rook		Herbert Frey
Northeastern Univ., Boston, Mass.	1	R. G. Porter	John Taylor	A. P. Sieklucki
Northwestern Univ., Evanston, Ill.	5	R. W. Jones	A. J. Hayes	G. Gauer
Norwich Univ., Northfield, Vt.	1	F. A. Spencer	J. S. Allen	W. Flint
Notre Dame, Univ. of, Notre Dame, Ind.	5	J. A. Northcott	G. A. Charters	R. J. Martina
Ohio Northern Univ., Ada, Ohio	2	D. S. Pearson	Ralph Walters	H. A. Ault
Ohio State Univ., Columbus, Ohio	2	S. O. Evans	John Birch	Robert Bergmann

Student Branches of the Institute (continued)

Name and Location	District	Counselor (Member of Faculty)	Chairman	Secretary
Ohio Univ., Athens, Ohio	2		Thomas Raymond	
Oklahoma A. & M. Col., Stillwater, Okla	7	A. Naeter		
Oklahoma, Univ. of, Norman, Okla	7	R. A. Church	Wm. Kerth	Robert Clark
Oregon State Col., Corvallis, Oreg	9	A. L. Albert	W. W. Shipley	L. A. Long
Pennsylvania State Col., State College, Pa	2	P. X. Rice	M. J. Borteck	M. P. Haden
Pennsylvania, Univ. of, Philadelphia, Pa	2	S. R. Warren, Jr	B. B. Bycer	W. W. Weinstock
Pittsburgh, Univ. of, Pittsburgh, Pa	2	R. C. Gorham	Richard Plaisted	Wm. Finch
Porto Rico, Univ. of, Mayaguez, P. R	3	M. Wiewall, Jr.		
Pratt Institute, Brooklyn, N. Y	3	D. H. Wright	George Morrow	P. C. Treuenfels
Princeton Univ., Princeton, N. J	2	W. C. Johnson	R. G. Mills	G. R. Compton
Purdue Univ., Lafayette, Ind	5	R. B. Marshall	M. R. Pagliarulo	L. R. Sjoblom
Rensselaer Poly. Inst., Troy, N. Y	1	E. D. Broadwell	Chun Foo	A. H. Shaw
Rhode Island State Col., Kingston, R. I	1		Donald Campbell	
Rice Institute, Houston, Texas	7	C. F. Wischmeyer	H. M. Rich	W. H. Newman
Rose Poly. Inst., Terre Haute, Ind	5	C. C. Knipmeyer	B. Vonderschmitt	Gene Landes
Rutgers Univ., New Brunswick, N. J	3	J. L. Potter	M. Taubenslag	Wm. Blauvelt
Santa Clara, Univ. of, Santa Clara, Calif	8	W. J. Warren	J. J. Radigan	S. A. Menzemer, Jr.
South Carolina, Univ. of, Columbia, S. C	4	Samuel Litman	B. C. King	D. O. Morgan
South Dakota State Col., Brookings, S. D	5	Wm. H. Gamble	R. J. Huntemer	J. K. Kopperud
So. Dakota State School of Mines, Rapid City, S. D	6	E. E. Clark	Walter Johnson	
Southern California, Univ. of, Los Angeles, Calif	8	P. S. Biegler	R. M. Pitts	Helen Burchell
Southern Methodist Univ., Dallas, Texas	7	E. H. Flath	L. S. Pully	H. J. Smith
Stanford Univ., Stanford University, Calif	8	H. H. Skilling	Wm. J. Wallace	E. D. Regan
Stevens Inst. of Tech., Hoboken, N. J	3	Wm. L. Sullivan	Lester Dreyfuss	Wm. Derbyshire
Swarthmore Col., Swarthmore, Pa	2		Richard Chambers	Evelyn Granat
Syracuse Univ., Syracuse, N. Y	1	C. W. Henderson	Wm. Spack	Doris Bryant
Tennessee, Univ. of, Knoxville, Tenn	4	W. O. Leffell	James Tombras	N. Feldman
Texas A. & M. Col., College Station, Texas	7	M. C. Hughes	P. Q. Olschner	Philip Kosub
Texas Technological Col., Lubbock, Texas	7	C. V. Bullen	Jack Horton	Holice Pruett
Texas, Univ. of, Austin, Texas	7	A. W. Straiton	L. C. Singleton	R. G. Dales
Tufts Col., Tufts College, Mass	1	A. H. Howell	Wm. Forbes	John Morse
Tulane Univ., New Orleans, La	4	M. G. Zervigon	Seymour Cambias	Alan Pincus
Union Col., Schenectady, N. Y	1	H. W. Bibber	Walter Short	Philip Ziffer
Utah, Univ. of, Salt Lake City, Utah	9	O. C. Haycock	A. L. Scott	R. W. Johnson
Vanderbilt Univ., Nashville, Tenn	4	S. R. Schealer	H. M. Bailey	F. T. Ragan, Jr.
Vermont, Univ. of, Burlington, Vt	1	E. R. McKee	D. L. Clark	R. F. O'Grady
Villanova Col., Villanova, Pa	2	H. S. Bueche	G. Hoberg	J. Gerngross
Virginia Military Inst., Lexington, Va	4	J. S. Jamison	V. J. Thomas	
Virginia Poly. Inst., Blacksburg, Va	4	Claudius Lee	R. B. Jackson	F. L. Crow
Virginia, Univ. of, University, Va	4	J. S. Miller, Jr	C. F. Kosbob	L. R. Jones
Washington, State Col. of, Pullman, Wash	9	H. F. Lickey	John Johnson	Robert Anderson
Washington, Univ. of, Seattle, Wash	9	R. E. Lindblom	A. W. Kessler	L. D. Barter
Washington Univ., St. Louis, Mo	7	D. A. Fischer	Wm. Wheeler	Robert Sunderman
West Virginia Univ., Morgantown, W. Va	2	A. H. Forman	G. M. Powers	P. F. Hostein, Jr.
Wisconsin, Univ. of, Madison, Wis	5	G. F. Tracy	W. Altschwager	W. S. Toth
Worcester Poly. Inst., Worcester, Mass	1	R. K. Houston	E. V. Kosso	C. W. Bergman, Jr.
Wyoming, Univ. of, Laramie, Wyo	6	R. O. Trueblood	E. R. Allen	
Yale Univ., New Haven, Conn	1	A. G. Conrad	R. C. Andrews	B. W. Knollenberg

Total Branches..............125

Number of Enrolled Students November 1, 1944—3,318

Geographical District Executive Committees

District	Chairman (Vice-President, AIEE)	Secretary (District Secretary)	Chairman, District Committee on Student Activities
1 North Eastern	R. T. Henry, 303 Electric Bldg., Buffalo, N. Y.	Victor Siegfried, American Steel & Wire Co., Electrical Cable Works, Worcester, Mass.	R. G. Porter, Northeastern University, Boston, Mass.
2 Middle Eastern	W. E. Wickenden, Case School of Applied Science, Cleveland 6, Ohio	H. R. Vaughan, Westinghouse Elec. & Mfg. Co., East Pittsburgh, Pa.	P. X. Rice, Pennsylvania State College, State College, Pa.
*3 New York City	J. F. Fairman, Consolidated Edison Co. of New York, Inc., 4 Irving Place, New York 3, N. Y.	C. S. Purnell, Westinghouse Elec. & Mfg. Co., 40 Wall St., New York 5, N. Y.	D. H. Wright, Pratt Institute, Brooklyn, N. Y.
4 Southern	C. W. Ricker, Tulane University, New Orleans, La.	F. E. Johnson, Jr., 317 Baronne St., New Orleans 9, La.	W. O. Leffell, University of Tennessee, Knoxville, Tenn.
5 Great Lakes	M. S. Coover, Iowa State College, Ames, Iowa	N. C. Pearcy, Public Utility Engg. & Service Corp., 231 S. LaSalle St., Chicago 4, Ill.	M. M. Cory, Michigan State College, East Lansing, Mich.
6 North Central	L. A. Bingham, University of Colorado, Boulder, Colo.	Platt Wicks, University of Colorado, Boulder, Colo.	O. E. Edison, University of Nebraska, Lincoln, Nebr.
7 South West	R. W. Warner, University of Texas, Austin, Tex.	S. R. Friedsam, 3612 Bonnie Road, Austin, Tex.	M. C. Hughes, A. & M. College of Texas, College Station, Tex.
8 Pacific	J. M. Gaylord, Metropolitan Water District of Southern California, 306 W. Third St., Los Angeles 13, Calif.	Mark A. Sawyer, Southern California Tel. Co., 740 S. Olive St., Los Angeles, Calif.	F. W. Maxstadt, California Institute of Technology, Pasadena, Calif.
9 North West	C. B. Carpenter, Pacific Tel. & Tel. Co., 730 S. W. Oak St., Portland, Oreg.	C. C. Boozier, Westinghouse Elec. & Mfg. Co., 309 S. W. Sixth Ave., Portland 4, Oreg.	L. D. Harris, University of Utah, Salt Lake City, Utah
10 Canada	Walter J. Gilson, 1244 Dufferin St., Toronto, Ont.	W. Roy Harmer, H.E.P.C. of Ontario, 620 University Ave., Toronto, Ont.	

* District 3 includes all foreign countries except Canada.

Each District executive committee includes also the chairmen and secretaries of all Sections within the District, and the District vice-chairman of the national membership committee.